PROCEEDINGS OF THE CSNI
SPECIALIST MEETING
ON TRANSIENT
TWO-PHASE FLOW

"CURRENT ISSUES IN SYSTEM THERMAL-HYDRAULICS"

Held at Aix-en-Provence, FRANCE
April 6 - 8th, 1992

Edited by
M. REOCREUX
and
M.C. RUBINSTEIN

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Proceedings of the CSNI Specialist meeting on transient two phase flow, organised by the "Commissariat à l'Energie Atomique (CEA), Institut de Protection et de Séreté Nucléaire (IPSN), Centre d'Etudes Nucléaires de Cadarache (CEN) and held at Aix-en-Provence, France, 6-8 April 1992.

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CEA-IPSN/DRS Centre d'Etudes Nucléaires de Cadarache
Saint Paul-lez-Durance, France
SPECIALIST MEETING ON TRANSIENT TWO PHASE FLOW
"CURRENT ISSUES IN SYSTEM THERMAL HYDRAULICS"
Aix-en-Provence 6-8 April 1992
Edited by Documentation of Cadarache - CEA
Saint-Paul-Lez-Durance - FRANCE
ISBN-2-7272-0157-

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The greater part of the CSNI co-operative programme is concerned with safety technology for water reactors. The principal areas covered are operating experience and the human factor, reactor system response during abnormal transients, various aspects of primary circuit integrity, the phenomenology of radioactive releases in reactor accidents, and risk assessment. The Committee also studies the safety of the fuel cycle, conducts periodic surveys of reactor safety research programmes and operates an international mechanism for exchanging reports on power plant incidents.

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FOREWORD

On behalf of the OECD Nuclear Energy Agency, it is my pleasure to welcome all of you to this Fourth CSNI Specialist Meeting on Transient Two-Phase Flow organized in collaboration with the Safety Research Department of the CEA/IPSN. The previous Two-Phase Flow Meeting was held in 1981 in Pasadena, USA. During the ten years since that meeting we have seen a transition from a period dominated by the development of advanced thermal hydraulic computer codes supported by extensive experimental research towards a period of code application with diverging opinions about appropriate research strategies. At this stage it appeared very useful to gather together the best thermal hydraulic specialists to discuss the current situation and investigate whether a consensus could be reached on how future activities should be planned making best use of the relevant research of the past twenty years and the corresponding large investment of resources. Consequently, a new Transient Two-Phase Flow Meeting was proposed by the Principal Working Group No. 2 on Coolant System Behaviour and endorsed by the Committee on the Safety of Nuclear Installations (CSNI).

In order to be able to focus better on the most important issues and draw useful conclusions it was decided to invite all the papers to be presented, increase preplanning and reserve ample time for discussion. While this turned out to cause additional difficulties and work prior to the meeting, we hope that the outcome will justify these extra efforts.

I would like to emphasize that the CSNI Specialist Meetings in general should be less formal and more practical than most other meetings and conferences. Therefore, please take advantage of this opportunity for exchange of information, experience and views. In particular, I encourage discussion on the role of thermal hydraulic research today and in the future, and on what concrete actions should be undertaken now. This will also be the main item for the Final Discussion at the end of the Meeting, which, I hope, all of you will be able to participate in.

Finally, I would like to thank the CEA/IPSN/DRS very much for organizing this Specialist Meeting. Special thanks are due to Dr. ROCREUX, the Chairman, and to Mrs RUBINSTEIN and Mrs. ZEYEN, the practical organizers, who all worked hard to have everything ready for you today. I would also like to thank the members of the Programme Committee and the Session Chairmen for their invaluable contributions.

I look forward to a fruitful meeting. 

H. HOLMSTRÖM
OECD NEA Secretariat
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INTRODUCTION

The 4th Specialists' Meeting on Transient Two Phase Flow was held in Aix-en-Provence on April 6th-8th, 1992, organized by the Safety Research Department of the French Nuclear Safety and Protection Institute at the request of the OECD Committee for the Safety of Nuclear Installations. After Toronto in 1976, Paris in 1978 and Pasadena in 1981, the Aix-en-Provence meeting was in keeping with the course of studies initiated by the Thermalhydraulic Systems Behavior Task Group of the Principal Working Group N°2 for discussing the achievements and defining the needs of safety research in accident thermalhydraulics.

60 Specialists from 14 Countries (Belgium, Canada, Finland, France, Germany, Italy, Japan, the Netherlands, the United Kingdom, the USA, Spain, Sweden, Switzerland, Taiwan) attended the meeting, representing a large spectrum of experts from National Safety Authorities, Research Laboratories, Universities, Vendors and Utilities.

These specialists had to review the 15-year research period which had elapsed since the last meetings. This period had been characterized by the issuance of the large thermalhydraulic computer codes for LWR accidents, the performance of several hundreds of separate effect tests for the development and the qualification of the physical models, the carrying-out of the large experimental programmes on system loops (up to scale 1) for verifying the computer codes.

Although this research was mainly characterized by remarkable success, limitations still exist. In a safety approach, there need to be well identified and handled, and the specialists were asked to exchange their views in order to determine which solutions they expected to be affordable in the future.

Safety applications have already started which use these latest research achievements. They raise specific problems such as the use of validation matrices, the evaluation of uncertainties, the identification and the control of unavoidable users' effects. The specialists were required to exchange their experience of applications and to define how to improve them in the future.

Finally, one cannot consider the future without a careful review of the new and important problems raised by the safety issues of existing plants in Eastern Countries, as well as the new concepts of future plants and the increasing requirements for more detailed evaluations of severe accidents. These prospective areas were on the programme of the specialists in order that they define future needs and that they determine the research needed to satisfy them.

The papers upon which this Specialist meeting was based, all invited, together with the exchanges of views and experiences which took place, undoubtedly succeeded in reaching the planned objectives. The present proceedings are the indication of how well this was accomplished.

At this stage, I would like to particularly thank Mrs Marie Claire Rubinstein and Mrs Evelyne Zeyen who took charge of all organizational matters with such efficiency that the meeting was held in the best working conditions. I would also like to thank the Programme Committee Members who defined the detailed content of the meeting, the Session Chairmen who led the discussions, and the OECD/CSNI Secretariat without whose help the meeting would not have taken place.

M. REOCREUX
General Meeting Chairman
SESSION 1

OUTSTANDING ISSUES OF FUNDAMENTAL TWO-PHASE FLOW MODELLING

Session Chairmen:
G. Yadigaroglu, Professor ETH, Zürich
J.C. Micaelli, CEA/DRN, Grenoble
SESSION 1

SUMMARY

G. Yadigaroglu, J.C. Micaelli

Session 1 covered outstanding issues in two-phase flow modelling. Three main categories of concerns were identified:
- modelling of the physical phenomena,
- instrumentation,
- numerical methods.
Success in all three areas would be required to close the outstanding issues.

MODELLING

Four specific areas were chosen to illustrate the present needs for modelling in two phase flow: instabilities in BWRs, the multiphase flow aspects of fuel coolant interactions, reflooding and condensation phenomena.

The paper on BWR stability problems showed how the coupling of thermohydraulic instabilities with complex neutronic feedbacks via the density reactivity coefficient produces instabilities in BWRs. Various instability modes have been observed; these are often coupled. The challenging analytical and code-related issues raised for an efficient treatment of these problems were identified and further research requirements were discussed. Better stability criteria than the decay ratio which has been used in the past may be available.

The paper on fuel coolant interactions (FCI) considered three areas of importance: fuel melt quenching in water pools, addition of coolant to degraded cores, and FCI energetics. These areas are being actively investigated but the difficulties associated with the experimental measurements clearly set limits on the detailed modelling. Due to these limitations the results of this research cannot yet be integrated into severe accident analysis codes.

The paper on reflooding attempted to draw consensus on the best way of modelling reflooding situations. Current reflood modelling capability is reasonable, can be made sufficiently conservative for safety assessments, but is not really outstanding. Indeed, fundamental understanding of the detailed two-phase flow and heat transfer mechanisms and their implementation in codes have not progressed much for several years.

New challenges presented by additional accident scenarios now under consideration and the analysis of novel and Eastern reactor types makes the lack of full description of certain aspects of the reflooding problem more apparent. The challenge should be met by building models of the physical processes into the codes at the most fundamental practical level. Thus the potential of the BE codes to extrapolate into predictions of new situations could be best utilized.

Very important and useful information gathered from the coordinated international programme of large-scale system reflooding tests is now available and the codes should also be able to account properly for the observed multidimensional effects.
Condensation plays an important role in LOCA analysis, in particular during ECC injection and refill. Indeed it sets the initial conditions for reflooding of the core and affects the oscillations during reflooding. Violent condensation occurring at high mass transfer rates (e.g. during ECC injection) is not properly modelled since it would have required detailed consideration of liquid plugs, turbulent diffusion mechanisms etc. Such fine detail is not yet available in the codes.

At lower injection flow rates, modelling condensation as a local effect is possible. Nevertheless difficulties appear due to lack of data on different geometries and scales. Furthermore the effect of noncondensibles is not well quantified.

INSTRUMENTATION

The presentation on two-phase flow instrumentation has shown that accurate and useful measurements of macroscopic behaviour can be performed. Such measurements are possible when the physical scale of the phenomena is not too small. Redundant instrumentation can be installed and the signals are analysed using a data reduction algorithm. Extensive in situ calibration of such instruments is still required.

Good progress in two-phase instrumentation was made in the last 10-15 years, partly driven by oil industry needs. There are further development needs for very low flow rates, high void fractions, on-line gas mixture composition and water phase additives. Miniaturization of video equipment opens new avenues for visual observations.

On the other hand, there has not been much progress in the measurement of microscopic interfacial parameters. The potential of modern measurement techniques may not have yet been fully applied in this area.

CODES AND NUMERICAL TECHNIQUES

It was generally agreed that the Best Estimate (BE) two-fluid codes constitute indispensable tools for safety analysis. The current codes must still be used by skilled analysts however, in order to produce reliable results.

There has been remarkable convergence in the development of numerical techniques for the current generation of two-fluid BE codes (RELAP5, TRAC, CATHARE and ATHLET). It is therefore not surprising that still existent deficiencies and limitations are common to all these codes. Some of the limitations are inherent to the techniques used and cannot be overcome. These codes may therefore have to be supplemented by more specialized codes addressing specific issues (e.g. turbulent mixing problems).

One limitation appears when the physical processes have long relaxation times (examples are stratification, bubble coalescence, etc ...). Currently the closure relationships used in the codes for modelling all situations are algebraic. Proper treatment of the cases mentioned may require inclusion of additional derivatives or/and integral terms in the closure equations.

Present 3-D codes were written to describe global multi-dimensional phenomena at the macroscopic level. Thus small-scale 3-D effects (such as the detailed consideration of droplets in dispersed flows or the effects of flow singularities and turbulent mixing) cannot be considered.

Most of the remaining code deficiencies are related to the prediction of local phenomena characterized by steep parameter gradients (void fraction in mixture levels, pressure in critical flows, temperature in quench fronts and in condensation areas). Treatment of such local discontinuities presenting very steep gradients are not properly addressed by any available code. This limitation has been known for a long time but no real progress has been made. Alternative, quite different methods may be required in such cases.
Partial improvement is expected by finer nodalization, but this is not a universal remedy due to limitations in computing time, numerical stability etc. The large numerical diffusion introduced by the numerical schemes presently used is mainly responsible for these problems. In order to resolve them, it was suggested to look beyond current practice into the potentially powerful techniques that have been developed recently for high Mach number gas dynamics known as "high resolution" methods. Such methods consider the characteristics of signal propagation within the framework of a fully conservative scheme and provide the capability to track discontinuities.

Considering our extensive current experimental data base and knowledge of numerical modelling techniques, combined with the new generation of high speed, massively parallel computers, the present generation of "best-estimate" computer codes no longer provides the best estimates that can be made. A new generation of basic experiments, combined with the development of an advanced code containing non-diffusive numerics and additional dispersed liquid and vapour field equations, may be appropriate. The complexity of this task should not be underestimated. Careful planning, based on current knowledge of the development of complex systems, is necessary. Well-structured programming will be required along with automatic code generation. Given the high cost and long-term commitment required for such a task however, a clear need for such an undertaking should be demonstrated. Whether our current codes meet anticipated needs or could meet them with minor modifications, must first be determined.
COUPLED THERMOHYDRAULIC-NEUTRONIC INSTABILITIES
IN BOILING WATER NUCLEAR REACTORS:
A REVIEW OF THE STATE OF THE ART

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ABSTRACT

This paper provides a review of the current state of the art on the topic of
coupled neutronic-thermo hydraulic instabilities in boiling water nuclear reactors
(BWRs). The topic of BWR instabilities is of great current relevance since it
affects the operation of a large number of commercial nuclear reactors. The
recent trends toward introduction of high efficiency fuels that permit reactor
operation at higher power densities with increased void reactivity feedback and
decreased response times, has resulted in a decrease of the stability margin in
the low-flow, high-power region of the operating map. This trend has resulted
in a number of "unexpected" instability events. For instance, United States
plants have experienced two instability events recently, one of them resulted in
an automatic reactor scram; in Spain, two BWR plants have experienced unstable
limit cycle oscillations that required operator action to suppress. Similar
events have been experienced in other European countries. In recent years, BWR
instabilities has been one of the more exciting topics of work in the area of
transient thermo hydraulics. As a result, significant advances in understanding
the physics behind these events have occurred, and a "new and improved" state of
the art has emerged recently.

INTRODUCTION

Only a few years ago, the topic of instabilities in boiling water reactors (BWRs)
was only of interest to a few thermal hydraulic scientist that just happened to
work in the area; however, recent events in which reactors underwent self
sustained oscillations that caused an unexpected scram have revived the interest
in this topic. Two events have contributed mostly to this renewed interest: the
first of these events occurred in 1984 at the Caorso plant1, and it was the first
widely reported case in which a reactor oscillated in the out-of-phase mode,
where half the core radially increases power while the other half decreases,
and the average power remains essentially constant, but the local power may
undergo severe oscillations; indeed, the TVO-I plant2 had already oscillated in
this mode in 1978, but the results were not widely known outside Sweden, so that
most of the "credit" for out-of-phase oscillations is given to Caorso. The

* Managed by Martin Marietta Energy Systems, Inc., for the U. S. Department
of Energy under Contract No. DE-AC03-84OR21400
second event that contributed to the renewed interest was the LaSalle event,\textsuperscript{2} that occurred in March 1988, and resulted in an unexpected reactor scram; other European plants had events that resulted in unexpected scrams before LaSalle, but

since LaSalle is located in the United States (US), the US Nuclear Regulatory Commission (NRC) took notice and started a long-term program with the BWR Owner's Group (BWROG) to solve the stability problem\textsuperscript{3}. Partly because of the NRC leadership in the nuclear industry, and partly because of the events that had happened in Europe over the years, interest on this topic has grown significantly in the past few years. For instance, in a recent international workshop on BWR stability held in Long Island (US), there were more than 100 participants, most of which presented papers related to their efforts in the area; the proceedings from that workshop are probably the best current reference that one can have in this topic.

As stated before, there have been about a couple of dozen of instability events in commercial BWRs; fortunately most of them have occurred during special stability tests, but approximately 25\% of them have occurred during regular operation. It is hard to count the number of instability events because, in most cases, is only reported as an unusual event to the corresponding regulatory agency and the event does not get much publicity. For example, it is hard to find an open literature reference to even the most well-known event, the one at LaSalle. Nevertheless, references 1 through 25 describe some of the stability events and tests that have been performed during the years. All the known tests and events have occurred in the United States and Europe; there has been a significant analysis effort devoted to this subject in Japan\textsuperscript{26-31} and Taiwan\textsuperscript{32} but until present, no instability events have occurred in Asian reactors. A significant effort has been devoted in the US to understand the basic behavior of BWRs under unstable conditions\textsuperscript{33-39}, specially the behavior of subcritical modes that lead to out-of-phase oscillations\textsuperscript{35-36}.

This paper presents a tutorial on the physical mechanisms that lead to instabilities in BWRs, with special emphasis on the differences between the expected oscillation modes. A review of the computer codes used by industry is presented, along with and application to the Cofrentes\textsuperscript{25} instability event.

**OBSERVED INSTABILITY MODES IN BWRS**

It is generally recognized that BWRs are susceptible to three types of instabilities:

1. **Control system instability.** These are due to out-of-tune controllers. This is a malfunction of the reactor hardware and is easily corrected by adjusting the controller gains.

2. **Channel thermohydraulic instability.** A heated channel in a two-phase-flow regime can oscillate on its own, without the need of neutronic feedback, if the local axial pressure drops become out-of-phase with the inlet flow perturbations due to the density-wave effect.

3. **Coupled neutronic-thermohydraulic instability.** This type of instability is also called reactivity instability because it involves the effect on
the neutronics of a density wave through the reactivity effect of the voids.

The difference between the channel (type 2) and reactivity (type 3) instabilities is that in the second one there is a power feedback in addition to the flow feedback. Thus, in general, channel instabilities are not likely in BWRs because reactivity instabilities dominate the reactor response due to their additional neutronic feedback.

The type of instability most relevant for safe BWR operation is the reactivity instability. Control system instabilities are easily corrected once detected by adjusting the controller gains, and channel instabilities should only be of concern under very special situations, such as partial blockage of a single channel. Two types of reactivity instabilities have been observed in commercial BWRs:

1. **Core-wide reactivity instabilities.** In this type of instability, the whole core behaves as one, and the oscillations are in-phase across the core.

2. **Out-of-phase reactivity instabilities.** In this type of instability, half of core, radially, behaves out of phase from the other half. That is, when the power rises on one half of the core, it is reduced on the other one by approximately the same amount, so that the average power remains essentially constant.

**PHYSICAL MECHANISMS LEADING TO INSTABILITIES IN BWRs**

This section describes the physical mechanisms that lead to the observed instabilities in BWRs. Analyses and operating experience has shown that the most probable instabilities in commercial BWRs are either the channel flow instability or the coupled neutronic-thermohydraulic instability. Both of these types of instability have their roots in the density wave mechanism, which adds a significant delay to both the flow and density reactivity feedback paths. Thus, this section describes first the density wave mechanism, which governs the inlet flow feedback, and later the neutronic feedback which determines the power level.

**The Density Wave: Flow Oscillations**

The basic mechanism causing flow instabilities in BWRs is the so-called density wave, whose effect on pressure drop is illustrated in Fig. 1. The coolant in commercial BWRs flows in the upward direction through the core and is guided by bundle boxes that surround a matrix of fuel pins. Thus, variations in density in the bottom part of the channel travel upwards with the flow. For instance, if the inlet flow is decreased while keeping the channel power constant, there is an increase in the number of voids in the channel that will travel upwards as a packet forming a propagating density wave. This packet of voids produces a change in the local pressure drop at each axial location, which is delayed axially by the density wave propagation time (i.e., the effective time for the voids to move upwards through the core). In two-phase flow regimes, the local pressure drop is very sensitive to the local void fraction, and it is very large at the outlet of the channel where the void fraction is greatest; thus, a significant part of the pressure drop is delayed with respect to the original
Fig. 1. Illustration of the local pressure drop delay introduced by the density wave mechanism.

perturbation.

If the inlet flow is perturbed sinusoidally as illustrated in Fig. 1, the local pressure drops are also sinusoidal (within the linear range), but they are delayed with respect to the perturbation. The total pressure drop across the channel is the sum of a series of delayed sinusoids (the local pressure drop) and, thus, has also a sinusoidal form that is delayed with respect to the flow perturbation. If the channel outlet pressure drop (the one that is more delayed) is larger than the inlet pressure drop, then the total pressure drop may be delayed 180° with respect to the inlet flow and, thus, have the opposite sign. This is the case in Fig. 1, where an increase in inlet flow results in a decrease of channel pressure drop. One might think that this channel behaves as if it had a "negative" effective friction coefficient at this particular frequency; thus the channel flow is unstable and any inlet flow perturbation feeds on itself (positive feedback) and oscillations grow at that unstable frequency. The critical point at which the channel flow instability starts is when the outlet (i.e., delayed) local pressure drop equals the pressure drop at the inlet at a particular frequency. In this case, we have a channel with an effective zero friction at that frequency so that any perturbation sustains itself.

The relative flow stability of a channel will depend on the amount of inlet flow feedback that is a function of the channel boundary conditions. There can be three main types of boundary conditions: (1) constant pressure drop, (2) variable pressure drop, and (3) constant inlet flow.
A constant pressure drop boundary condition may be achieved in a test stand by having a large bypass flow in parallel with the test channel. In a BWR, if a single channel were to become unstable, the boundary condition would be forced by the remaining 799 bundles and it would remain essentially constant across the unstable channel. This type of boundary condition is the most unstable of the three, because it results in the largest amount of inlet flow feedback oscillations required to maintain the constant pressure drop.

A variable pressure drop boundary condition results when the channel inlet flow is determined by the recirculation loop and pump dynamics. In this case, the channel pressure drop at a particular flow and time must match the pressure drop across the recirculation loop plus the pressure gain at the pump for the particular loop flow at every time. For this pressure balance, dynamic terms, such as inertia, have to be taken into account in the channel as well as in the circulation loop. Since the pressure drop across the channel is allowed to oscillate according to the recirculation loop dynamics, the inlet flow feedback is not as strong in this case as in the constant pressure drop boundary condition.

A constant inlet flow boundary condition can be achieved in a test stand by having a constant displacement pump feed the channel a constant inlet flow regardless of pressure. This is the most stable boundary condition. Indeed, since the flow is constant, it can not oscillate so that the channel is always stable. This condition cannot be achieved in commercial BWRs.

The physical processes that cause flow oscillations are hard to visualize, but they are slightly easier for the case of the constant pressure drop boundary condition: Let's assume an unstable channel that, as described before, has an effective negative friction coefficient at a frequency. Let's further assume that the pressure drop across this channel is maintained constant by, for instance,

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**Fig. 2.** Block diagram of the feedback paths for the coupled neutronics-thermohydraulics instability type.
maintaining a large flow through a bypass region in parallel with the channel. Then, if a perturbation occurs that increases the inlet flow, the negative effective friction coefficient would tend to decrease the pressure drop. To compensate and keep the pressure drop constant, the channel increases the inlet flow even further, which causes a runaway instability. What complicates the fact is that the effective friction coefficient is only negative at a particular frequency; thus, the flow increase is not an average flow increase, but a flow increase at the frequency. In other words, the instability results in an oscillation of frequency with a runaway, exponentially growing amplitude. Nonlinearities in the system eventually cancel the growth of the oscillation and a limit cycle is established at a finite oscillation amplitude.

Neutronic Feedback during Oscillations

In the previous section, we have described the density wave in relation to flow instabilities. For those instabilities, only flow is involved and the power generation term in the fuel is assumed constant. In BWRs, the power generation is directly related to the neutron flux, which is a function of the reactivity feedback and, therefore, depends strongly on the core average void fraction. Thus, when a void fraction oscillation is established in a BWR, the power oscillates according to the neutronic feedback. This effect can be understood from Fig. 2, which shows a block diagram of the dynamics in a BWR.

The neutronic feedback path is somewhat different from the inlet flow feedback path. The neutronic feedback involves: (1) the neutron dynamics, which determine the power generated in the fuel, (2) the fuel dynamics, which define the heat flux from fuel to coolant, (3) the channel thermal hydraulic, which characterize the void fraction response to changes in heat flux and that include the inlet flow feedback via the recirculation loop, and (4) the reactivity feedback dynamics that relate the void fraction distribution to a reactivity value that affects the neutron dynamics. This feedback paths are illustrated graphically in Fig. 2. One important difference between the neutronic feedback and the flow feedback paths is due to the fuel transfer function. Before the power generated by the neutronics can feedback through the moderator density, it has to change the fuel temperature to alter the heat flux from fuel to coolant. The fuel in commercial BWRs responds relatively slow with a time constant between 6 and 10 seconds. This results in a single pole break frequency of the order of 0.03 Hz. Since the unstable oscillations occur approximately one decade above this break
frequency (i.e., at approximately 0.3 to 0.5 Hz), the fuel adds almost 90° phase delay to the feedback. Furthermore, the gain of the fuel transfer function is rolling down at approximately one decade per decade for frequencies between 0.1 and 1 Hz. This effect can be seen in Fig. 3, which shows a typical power-to-heat-flux transfer function calculated by the LAPUR code. Thus, the fuel has some destabilizing effect because of its phase delay, but it also has a significant stabilizing effect due to its inherent filtering of the oscillation amplitude at frequencies higher than 0.1 Hz. Changes in fuel time constant affect the reactor stability in two ways, but experience has shown that the gain effect is dominant over the phase effect. Thus, decreasing the time response of the fuel (i.e., smaller diameter fuels, or increased pellet-clad gap conductance) has a destabilizing effect in general.

The void reactivity feedback is computed as a spatial averaging of the void distribution in the core weighted the local void reactivity coefficients and the local neutron flux and adjoint. In mathematical terms, the reactivity feedback, $\Delta \rho$, due to a void perturbation, $\Delta \alpha$, can be written as shown in Eq. (1)

$$\Delta \rho(z) = \int_0^N \left( \int_0^N \Phi^*(r,z) \Phi(r,z) \left( \frac{d\rho}{da} \right)(r,z) \Delta \alpha(r,z,t) \right) dr$$  \hspace{1cm} (1)

where $\Phi$ and $\Phi^*$ are the normalized neutron flux and its adjoint, respectively, and $d\rho/da$ is the local density reactivity coefficient.

The averaging described by Eq. (1) results in a phase delay of slightly over 90° and a large filtering effect (i.e., gain reduction) for frequencies higher than the inverse of the density wave time constant. Figure 4 illustrates this effect in the LAPUR-calculated transfer function from fuel surface heat flux to density reactivity feedback. The units in this figure are normalized to the DC (i.e., zero frequency) value because only the shape of the transfer function is relevant to this general discussion. The absolute gain value of this transfer function will depend on the particular characteristics of the reactor being modelled; for instance, the gain is directly proportional to the density reactivity coefficient. As it can be observed in Fig. 4, the filtering effect on the channel gain is very significant at high frequencies, and it results in a fast rolloff that, for all practical purposes, eliminates all frequencies higher than the fundamental oscillation frequency from the feedback path. This is the reason why it has been observed in time domain codes that the reactivity feedback is essentially sinusoidal even under large limit cycle conditions when the neutron flux has a significant amount of higher harmonic contamination. The filtering
Fig. 5. Flow patterns for the three main instability modes. Arrow thickness indicate flow intensity. Arrow to the right of vessel illustrates core pressure drop.
effect observed above is due to the spatial averaging introduced by Eq (1) combined with the density wave mechanism. For example, if the reactor power (and, consequently, the fuel heat flux) is oscillated at a high frequency, there will be an associated density wave formed by the void perturbations that will travel upwards through the channel. If the oscillation frequency is higher than the density-wave characteristic time delay, the wave front will not have time to leave the top of the channel before the next wave front is created. In this manner, when the average void fraction is calculated using Eq. (1), the positive and negative parts of the wave cancel each other and there is a significant decrease in overall density reactivity feedback. On the other hand, if the power oscillation is of a very low frequency, the spatial averaging does not produce the canceling effect described above and the gain is not reduced.

Boundary Conditions for Different Oscillation Modes

It has been established experimentally that momentum dynamics and the recirculation-loop flow path play an important role in defining reactor stability because, for the fundamental mode of oscillation, any change in power is accompanied by a change in inlet flow. The amount of this change is determined by momentum dynamics in the core and recirculation-loop characteristics. However, an out-of-phase mode of oscillation in parallel channels, does not require changes in total inlet flow because the two oscillating core regions adjust their flows to maintain equal pressure drops across the core. In other words, if the flow increases in channel 1, the flow of channel 2 decreases by the same amount (at least within the linear operating region) and the total flow remains unchanged. This mechanism allows for large flow oscillations within each channel, and it has the effect of increasing the gain of the thermohydraulic component in the BWR dynamics feedback, thus decreasing the reactor stability.

The mechanism described above is represented schematically in Fig. 5. In this figure, the arrows represent the flow intensity through the representative channels during an oscillation of period T. For the fundamental (core-wide) mode of oscillation the whole core behaves as a unit, and the total core inlet flow oscillates in phase with the core pressure drop. In the out-of-phase mode of oscillation a constant total core inlet flow is maintained by readjusting individual channel

![Fig. 6. Simplified block diagram of BWR dynamics showing the two main feedback paths](image-url)
flows, and the core pressure drop is maintained constant. When a channel instability occurs, the inlet flow of the single unstable channel oscillates, but the total core flow and core pressure drop remains essentially constant because it is controlled by the large number of stable channels.

Thus, the boundary conditions that must be used to model the three instability modes are as follows:

1. **Core-wide instability mode.** Variable pressure drop across the core that is determined by the recirculation loop dynamics.

2. **Out-of-phase instability mode.** Constant pressure drop across all channels in the core.

3. **Channel thermohydraulic instability mode.** Constant pressure drop across the channel.

A thermohydraulic model of a BWR includes fuel, core coolant, and recirculation-loop dynamics. The dynamic processes solved by most BWR stability codes can be summarized as follows: An energy balance in the fuel region yields the heat transferred to the core coolant. The energy and continuity balance equations are solved in the coolant region to obtain the core enthalpy (i.e., void fraction) distribution. Neglecting second-order effects, the momentum equation can integrate this distribution to yield the core pressure drop. The recirculation-loop momentum equation yields the core inlet flow from the pressure drop across the jet pumps that must equal the pressure drop across the core. The thermohydraulic loop is closed when the inlet flow is coupled to the coolant energy and continuity equations. Finally, the thermohydraulic and neutronic models are coupled via the fuel temperature and void reactivity feedbacks, which yields the $\delta \alpha$ term in Eq. (1). Thus, there are two main feedback paths in the closed loop dynamics of a BWR: (a) the inlet flow feedback, characterized by the reactor and recirculation loop momentum dynamics, and (b) the neutronic feedback, caused by the void reactivity coefficient. These feedback paths are represented graphically in Fig. 6.

In physical terms, the dominance of the out-of-phase and core-wide modes of unstable oscillations depends on the relative gains of the two feedback paths of Fig. 6. The out-of-phase instability mode has very large gain for the inlet flow feedback (in essence the flow can oscillate as much as it wants without having to pay any friction "penalty" in the recirculation loop), but it has low gain from neutronic feedback because it corresponds to a damped subcritical mode. For the core-wide instability mode the situation is reversed: the neutronic feedback is large, but the inlet flow feedback is smaller because flow oscillations are damped by the friction in the recirculation loop. The above effects are reflected schematically in Fig. 6, where the relative gain is represented by the arrow thickness. Thus, either of the two instability modes (out-of-phase or core-wide) may dominate the response of the reactor. Which of the two will dominate depends on specific values of parameters as they affect the relative gain of the two main feedback paths.

The thermohydraulic equations in a particular channel are essentially the same for the out-of-phase mode as for the core-wide (fundamental) mode; they are based on the momentum, energy, and continuity equations. The only difference between the two modes arises on the core boundary conditions (i.e., inlet flow and
pressure drop). For the core-wide mode, the boundary conditions are determined by the recirculation-loop dynamics. For the out-of-phase mode, however, the boundary conditions are fixed, and they determine the necessary inlet flow to maintain a constant pressure drop across the core. This is a well-known boundary condition for parallel channel oscillations and is caused by the common plena connecting all channels.

In frequency domain, linear codes, the constant-pressure-drop boundary condition can be implemented either by properly connecting the individual open loop transfer functions as they are combined to form the closed loop or by setting the gain of the recirculation-loop pressure-to-flow transfer function to an arbitrarily large number and using an existing core-wide stability code. Both methods yield the same result because both minimize pressure drop variations.

In summary, there are two competing effects in the out-of-phase mode: on the one hand, the neutronics component is subcritical and tends to damp out oscillations; on the other hand, the thermohydraulic component in the out-of-phase mode has more gain than in the fundamental mode and tends to destabilize it. The relative importance of the two above effects depends on the degree of subcriticality of the out-of-phase mode. Thus, it seems plausible that there is a threshold subcritical reactivity at which the out-of-phase mode can become unstable, even if the fundamental mode is stable.

CODES USED FOR BWR STABILITY CALCULATIONS

Predictive calculations of BWR stability are simply too complex to allow for simple calculations and, with a few honorable exceptions, the code to simulate the dynamic behavior of the reactor. The family of codes that have been used to predict the stability of commercial BWRs can be subdivided in two main categories: frequency-domain and time-domain codes. Among the frequency domain codes, one finds LAPUR,42-45 NUFREQ,46 and FABLE.47 Time-domain codes are more widely used; among them, one finds RAMONA-3B,48-50 TRAC-BF1,51 TRAG,52-55 RETRAN,56-57 EPA,58 SABRE,59 TRAB,2 TOEDYN-2,60 STANDY,27-29 and SPDA.61

LAPUR was developed at the Oak Ridge National Laboratory (ORNL) for the US NRC and is currently used by NRC, ORNL, and others; its current version is LAPUR-5. LAPUR's capabilities include both point kinetics and the first subcritical mode of the reactor for out of phase oscillations; the thermohydraulics part is modeled as up to seven flow channels whose inlet flows are coupled dynamically at the upper and lower plena to satisfy the pressure drop boundary condition imposed by the recirculation loop. LAPUR's main result is the open- and closed-loop reactivity-to-power transfer function from which a decay ratio is estimated.

NUFREQ is in reality a family of codes called NUFREQ-N, NUFREQ-NP, and NUFREQ-NPW that calculate reactor transfer functions for the fundamental oscillation mode; the main differences between them are the ability to model pressure as an independent variable (NUFREQ-NP) so that it can reproduce the pressure perturbation tests. NUFREQ-NPW is a proprietary version currently used by Asea Brown Boveri (ABB); its main feature is an improved fuel model that allows to model mixed cores.
FABLE is a proprietary code used by General Electric (GE) which can model up to 24 radial thermal hydraulic regions that are coupled to point kinetics to estimate the reactor transfer function for the fundamental mode of oscillation.

RAMONA-3B is a code that was developed jointly by the US NRC and ScandPower; it is currently used by Brookhaven National Laboratory (BNL), ScandPower, and ABB. RAMONA-3B has a full 3D neutron kinetics model that is capable of coupling to the channel thermal hydraulics in a one-to-one basis. Typically, in time-domain codes, the thermal hydraulic solution is orders of magnitude more expensive (in CPU time) than the neutronics; because of this expense, the thermal hydraulic channels are often averaged into regions to reduce computation time. RAMONA-3B uses an integral momentum solution that reduces significantly the computational time, and it allows for the use of as many computational channels as channel are in the core.

TRAC has two version currently used in BWR stability analysis. TRAC-BF1 is the open version used mostly by Idaho National Engineering Laboratory (INEL) and Pennsylvania State University. TRACG is a GE-proprietary version. TRAC-BF1 has one dimensional neutron kinetics capability (as well as point kinetics), TRACG has full 3D neutron kinetics capability (as well as 1D and point kinetics), and GE has incorporated most of their proprietary correlations. The numerics in TRACG have also been improved with respect to those in TRAC-BF1 to reduce the impact of numerical diffusion and integration errors. Typically TRAC runs are very expensive in computational time; to minimize this time, most runs are limited to the minimum number of thermal hydraulic regions that will do the job, typically 20.

RETRAN is a time domain transients code developed by the Electric Power Research Institute (EPRI). It has 1D and point kinetics capability. RETRAN is a relatively fast-running code due to the fact that it models a single radial thermal hydraulic region and uses the so-called three equation approximation (i.e., it assumes equilibrium between phases). A big advantage of RETRAN over other more detailed tools is that is capable of running in a desktop personal computer.

EPA stands for Engineering Plant Analyzer, and it is a combination of software and hardware that allows for real time simulation of BWR, including most of the balance of plant. EPA was developed by NRC and is located in BNL. EPA’s software for BWR stability simulations is named HIPA, and it models point kinetics with mainly an average thermal hydraulic region (a hot channel is also modeled but it does not provide significant feedback to affect the global results). HIPA uses modeling methods similar to those of RAMONA-3B and, in particular, it uses the integral momentum approach to speed up the thermal hydraulics calculation. An interesting feature of HIPA is the ability to use time dependent axial power shapes to compute the reactivity feedback; the nodal power shape is varied according to the local void fraction as a function of time based on some polynomial fits that are input to HIPA.

SABRE is a time domain code developed and used by Pennsylvania Power and Light for transient analyses that include BWR instabilities. SABRE uses point kinetics for the neutronics and a single thermal hydraulic region.

TRAB is a 1D neutronics code with an average thermal hydraulics region. It was developed and used in the Finish Center for Radiation and Nuclear Safety. It has
been benchmarked against RAMONA-3B calculations and a stability event in the TVO-I plant.

TOSDYN-2 has been developed and used by Toshiba Corporation. It includes a 3D neutron kinetics model coupled to a five-equation thermal hydraulic model. TOSDYN-2 models multiple parallel channels as well as the balance of plant.

STANDY is a time domain code used by Hitachi Ltd. It includes 3D neutron kinetics and parallel channel flow across at most 20 thermal hydraulic regions. STANDY is a vessel model only, and it does not include the balance of plant.

SPDA is a combination of RELAP5 and EUREKA, and it is used by the Japan Institute of Nuclear Safety. RELAP5 calculates the thermal hydraulic part of the solution, while the nodal power is estimated by EUREKA, which is a 3D neutron kinetics code.

REFERENCES


APPENDIX

SIMULATION OF THE COFRENTE N.P.P. INSTABILITY EVENT WITH LAPUR CODE.

Cofrentes N.P.P. is a 2894-MW boiling water reactor of the BWR-6 plant generation type designed by General Electric (GE). It has 624 fuel assemblies. At the time of the event (an out-of-phase instability with 13% peak-to-peak amplitude in APRM registers and no LPFM alarm, on January 1991), all fuel assemblies were 8 x 8 GE-6 and GE-7 fuel and the plant was recovering after an unprogrammed scram.

Input data for this LAPUR simulation were provided\(^1\) to the Consejo de Seguridad Nuclear (Regulatory Authority in Spain) by Iberdrola (facility owner). The data consisted of operating conditions (such as power, flow, subcooling, etc) together with some valuable design information used to adjust the nuclear model. Axial and radial distributions, and cross sections used to estimate density reactivity coefficients were calculated\(^2\) by the Core Analysis Unit of CSN with CASMO-SIMULATE code.

The reactor conditions used for this simulation were: power 40.8%, flow 30.7%, pressure 66.2 Kg/cm\(^2\) and inlet enthalpy of 1119 KJ/Kg (There had never been an instability event with such low power and flow conditions although the inlet subcooling was abnormally high due to problems with the start up of the preheater number 5). Seven thermohydraulics regions (or channels types) were used for the calculations, and Table A-1 summarizes some of their conditions. Thermohydraulic channel 1 has the highest power, and channel 7 has the lowest. Channels 1 through 6 represent the core center (although the number of the channel does not represent any radial distribution), and channel 7 represents the core periphery bundles with reduced power and increased inlet restriction in order to minimize their flow. It should be noted that the conditions for the Cofrentes event resulted in a fairly skewed radial power distribution and a bottom peaked axial power shape as can be observed in Table A-1. These two reasons were probably the main causes of the observed oscillations. With these conditions, the LAPUR code predicts a decay ratio of 0.88 for the core wide mode and 0.87 for the regional one\(^*\) (the out-of-phase decay ratio is based on a -1.0 subcritical reactivity value for the out-of-phase mode i.e. the eigenvalue separation between the fundamental and first neutronic modes is assumed to be equivalent to 1.0). These two results can be considered as unstable conditions due to code uncertainties and measure errors.

Once the base case represented in the paragraph above was achieved, some sensitive analyses were performed in order to see how different conditions of operation influenced in the decay ratios. The results are presented in figures A1 through A5 an in Table A-2.

Figure A1 represents the decay ratios obtained when varying the nuclear power while we keep the rest of the plant parameters constat. This should be equivalent to a change of the position of the control rod within the core, operation that was occurring when the oscillation took place. As can be

\(^*\)Studies made by General Electric after the incident showed that Cofrentes event had been of the out-of-phase type, although the core wide mode of oscillation should have been unstable too.
observed the decay ratios are higher as the power increases.

<table>
<thead>
<tr>
<th>Relative Power</th>
<th>Ch-1</th>
<th>Ch-2</th>
<th>Ch-3</th>
<th>Ch-4</th>
<th>Ch-5</th>
<th>Ch-6</th>
<th>Ch-7</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of bundles</td>
<td>24</td>
<td>60</td>
<td>204</td>
<td>124</td>
<td>108</td>
<td>28</td>
<td>76</td>
</tr>
<tr>
<td>Inlet Restriction</td>
<td>Low</td>
<td>Low</td>
<td>Low</td>
<td>Low</td>
<td>Low</td>
<td>Low</td>
<td>High</td>
</tr>
<tr>
<td>Axial Picking Fc.</td>
<td>2.13</td>
<td>2.13</td>
<td>1.86</td>
<td>1.69</td>
<td>1.69</td>
<td>1.69</td>
<td>1.69</td>
</tr>
</tbody>
</table>

Table A-1. Description of thermodydraulics regions used.

In Figure A2 we show the change in decay ratio when the parameter changed in core flow. In this case the tendency is towards lower DR’s when augmenting the core flow.

Both the results of figures A1 and A2 are well known and not very much attention must be paid to them, because the procedures for starting up the plant oblige the operators to get very near the stability border. The case is different with the other tests performed because these suggest changes in procedures or verifications that can avoid the oscillation.

In Figure A3 we present the results when changing the inlet temperature (or the subcooling, which is opposed to the previous one). As it has been stated above, the subcooling were abnormally high due to problems with the valves of preheater number 6, that was supposed to be in service when actually it did not. One of the effects of the oscillation has been to change the start up procedure, forcing the operator to be sure to have a minimum in feedwater temperature before to begin the change in the velocity of the recirculation pumps.

In Figure A4 we present the results when varying the axial picking factor (in this case we put all the channels with the same profile of axial power). This parameter is related with, and besides, it amplifies the effect of the inlet subcooling. This is the reason to introduce a new recommendation to avoid the axial profile too skewed downwards.

In Table A-2 we present other study consisting in the change of the radial distribution of the axial picking factors. As has been stated above, due to the sequence for extracting control rods, the situation of these in the moment of the oscillation was quite unsimmetrical within the core (some rods were completely inserted, while others were completely extracted). As can be seen in the Table the situation is different whether we look at the core wide mode of oscillation, in which case the decay ratio increases first to reach a maximum and then decreases while making the radial distribution more picked; or whether we observe the trend in regional mode which is always increasing its value. This is better shown in Figure A5 where some of the points of Table A-2 are represented. Operational experience shows that different strategies for extracting the control rods from the core could make that this effect were not so much pronounced, making the reactor more stable.
<table>
<thead>
<tr>
<th>APF=2.126</th>
<th>APF=1.862</th>
<th>APF=1.693</th>
<th>CORE WIDE DR</th>
<th>REGIONAL DR</th>
</tr>
</thead>
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<tr>
<td>0</td>
<td>0</td>
<td>624</td>
<td>.7286</td>
<td>.6318</td>
</tr>
<tr>
<td>24</td>
<td>0</td>
<td>600</td>
<td>.9005</td>
<td>.8592</td>
</tr>
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<td>84</td>
<td>0</td>
<td>540</td>
<td>.9050</td>
<td>.8696</td>
</tr>
<tr>
<td>288</td>
<td>0</td>
<td>336</td>
<td>.8366</td>
<td>.8711</td>
</tr>
<tr>
<td>624</td>
<td>0</td>
<td>0</td>
<td>.7868</td>
<td>.8727</td>
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<td>60</td>
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<td>0</td>
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<td>0</td>
<td>.7402</td>
<td>.7121</td>
</tr>
</tbody>
</table>

Table A-2. Influence of radial distribution of axial picking factors in core wide and regional decay ratios.

For last, in the same start up, it seems\(^3\) that the conditions of operation were within exclusion region for a second time although this time oscillations did not occur. This point were simulated with LAPUR too, and decay ratios of around 0.6 were obtained for both core wide and regional modes of oscillations. This is represented in Figure A6, which also shows the approximated region for out-of-phase oscillations used by General Electric after a series of studies made with TRAC-GE. In this figure we can see that with the decay ratios obtained with LAPUR the first point (that correspond with the initial oscillation) lies within the region of out-of-phase oscillations, while the second point (that corresponds with the second entrance in unstable region) correspond to a stable condition for this mode of oscillation.

CONCLUSIONS

Satisfactory agreement was found between the LAPUR calculated decay ratios and the observed events that took place in Cofrentes N.P.P. on January 1991. Moreover, the simulation has made possible to obtain a series of tendencies in DR's when changing some plant parameters.

The trends showed that the modifications and recommendations introduced
In start up procedures related with the achievement of a minimum feedwater temperature when operating nearby the unstable regions of BWR's as well as to avoid neutronic axial profiles too sharp skewed downwards, are essentially correct if we are trying to reduce the risk of entering the region of probable oscillations.

Besides, the study shows that another important factor concerning the onset of oscillations is the sequence of movement of control rods when trying to increase the reactor power. Possibly, a strategy based in a more simmetrical and equal control rod movement should be better to avoid unstable regions.

REFERENCES


**Another recommendations were related with the training of plant operators in order to better distinguish oscillations from noise, and making them aware of the importance of avoiding the exclusion regions.**
**Fig. A1.** Decay ratio vs. nuclear power.

**Fig. A2.** Decay ratio vs. core flow.
**Fig. A3.** Decay ratio vs. core inlet temperature.

**Fig. A4.** Decay ratio vs. radial picking factor.
Fig. A5. Decay ratio vs. radial distribution of axial picking factors.

Fig. A6. General Electric correlation for out-of-phase instabilities.
MODELING OF REFLOODING

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ABSTRACT

The state of the art in modeling reflooding situations, mainly with the two-fluid system analysis codes, is reviewed; certain related general code development issues are included. Our current modeling of reflooding is reasonable and can be made sufficiently conservative for safety assessments, but it is not outstanding. Fundamental understanding of the detailed two-phase flow and heat transfer mechanisms has not progressed significantly over the state already available several years ago.

The better understanding of system behavior achieved by the coordinated program of large-scale experiments is summarized and its impact on the modeling work discussed. In the future, factors such as the additional accident scenarios now considered, the new and advanced reactor types being analyzed, and the geometric growth of computing capacity are likely to drive our efforts. The new requirements and challenges can be met best by building into the codes pieces of understanding of the actual physical processes at the most fundamental level practicable.

The discussion focuses on the existing codes and their successes and shortcomings; both certain specialized and the more complex general-purpose system codes are considered. The aim is not to conduct an exhaustive review of all aspects of the problem, but rather to reach consensus on certain issues.
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1 INTRODUCTION

More than two decades after the first systematic efforts to understand reflooding phenomena and to provide analytical descriptions suitable for safety analysis, our current reflood modeling capabilities are reasonable and can be made sufficiently conservative for safety assessments, but they are certainly not outstanding. The main purpose of this paper is to review the state of the art in modeling reflooding situations with the large system analysis codes. The review is focused on the Pressurized Water Reactors (PWR); many of the reflooding phenomena can also be encountered in Boiling Water Reactors (BWR), however. Since the reflooding problem touches on a wide spectrum of thermal-hydraulic problems, consideration of certain more general code development issues was included.

Contemplating what has been achieved in the past and what remains to be done, we should note in the first place the shift in the basic approach from safety analysis based on conservative evaluation models (EM) and codes to best estimate (BE) assessments.

Since development of the advanced BE tools began, much progress was made in gathering information on PWR system behavior from a coordinated program of large-scale experiments; this was necessary and will continue to be very useful in terms of providing guidance for further code development and verification. However, one disappointing aspect of some of this work is that our fundamental understanding of the detailed two-phase flow and heat transfer mechanisms involved (and the corresponding implementation in code models) have not progressed significantly over the state already available several years ago.

Little progress was achieved due to principally two factors: first, the lack of emphasis on smaller separate-effect experiments addressing particular issues, and the fact that most experimental measurements needed for understanding the basic phase interaction phenomena and for providing better mechanistic models for them are still beyond the capability of modern instrumentation. Thus advanced models often remain largely untested in their detailed description of the phase interactions, and we are left relying only on global comparisons and model assessments. Second, direct extrapolation of experience gained from controlled-reflooding bundle tests to actual reflooding situations in reactors, involving all the parallel-channel and system-related complications, is not a straightforward task.

In the future, three factors are likely to drive our continuing efforts in modeling of reflooding:

a) additional accident scenarios which are now under consideration
b) new advanced reactors which are being designed
c) the geometric growth of computing capacity.

All three factors will tend to make our lack of a full understanding of certain aspects of the reflooding problem more apparent.

1.1 New Scenario and New Reactors

Regarding the first factor, indeed, new accident scenarios which are now under consideration extend the range of conditions under which reflooding may take place. Reflooding has been historically associated with large break Loss-of-Coolant Accidents (LB LOCA) as part of the design basis accident scenario for PWRs. Reflooding phenomena are relevant, however, for
an enlarged event spectrum that comprises also beyond-design-basis accidents. Core uncover is expected in a number of accident sequences where feedwater systems or Emergency Core Cooling (ECC) systems are unavailable or unduly delayed. Accident management measures, e.g. intentional depressurization, are foreseen to cope with such events. In these cases, a partially uncovered core may have to be reflooded under boundary conditions that are different from those of the classical low-pressure reflood. Calculation of severe accident sequences may even impose simulation of reflooding under degraded core conditions. Moreover, the changes associated with the move from EM to BE analyses do not only mean doing away with conservative assumptions in the reflooding models, but they also introduce a new set of more realistic core initial and boundary conditions that result from the best-estimate system behavior calculations that are possible now.

Not only has the spectrum of accidents to be analyzed widened considerably, but also alternate ECC systems for new generations of reactors, as well as other types of reactors have come into sight. With the new situation in Eastern Europe, certain countries in particular and the international community in general, are suddenly confronted with the need to analyze the full spectrum of accidents for three different types of pressurized-water VVER's and the boiling-water, graphite moderated RBMK reactor, for which the present codes have not been assessed yet. The new features introduced by these reactor types are not only different primary system configurations and ECC systems, but also differences in the core bundle configuration and cladding materials.

Finally, proposed advanced PWR's with tight lattices extend the range of geometrical and operating conditions that must be considered and require additional experimental information.

1.2 Largely Extended Computing Capacity

With the computing capacities available today, core-wide calculations involving a large number of representative fuel rods and a considerable number of hydraulic channels are feasible. Consequently, reflood modeling has now to take into account more complicated core hydrodynamics. Recent large system-effects experiments have laid the data base for code assessment with respect to these phenomena (see Section 3.2 below).

Paradoxically, modeling of reflooding may potentially become even more difficult by the advent of new generations of supercomputers. Codes currently available, such as TRAC, were designed 15 years ago for computers that no longer exist. They therefore run in a computationally very inefficient way on the current generation of machines. Timing estimates based on rewritten modules from these codes suggest that execution rates could be 7 to 10 times faster if the same codes were optimized for the current generation of computers (Nelson, 1992). Using new techniques such as multi-tasking, or multiple instruction/multiple data capabilities and the even further increased speeds of the next generations of machines, computation time reductions of orders of magnitude may be achievable.

History suggests that when such improved capability becomes generally available, the analyst not only runs his problems faster and more often, but he also expands his plant models to represent finer spatial detail. This finer degree of nodalization could, at least conceptually, produce a subchannel-type code replacing the current system-type code. The
difficulties then shift to questions of geometric scale or spatial averaging. There are already difficulties in providing closure laws "worthy" of the two-fluid models presently in use, as discussed below. Even if the analyst is prevented from nodalizing the system to the level of a subchannel, any codes specialized to the study of reflooding will certainly push the modeling capability to that level, and properly so. The finer-scale nodalization foreseen here presents an even greater challenge regarding the adequacy of the closure laws that will be needed for any future calculations.

1.3 Emphasis on Addressing the Physics at a Fundamental Level

The facts mentioned above could lead to a partly revised approach to modeling of reflooding; in any case, they pose new challenging requirements to our codes and models. These challenges can be met best by building into the codes pieces of understanding of the actual physical processes at the most fundamental level practicable, rather than by ad-hoc corrections and "fixes." Indeed the (potential) ability of today's BE codes to extrapolate beyond the available data base is one of the most significant accomplishments of the code development programs.

1.4 Differences in Basic Approach

From a code development point of view, there are two basic approaches that have been followed: attempts have been made to develop relatively simple and typically one-dimensional fast running codes to address reflooding problems and to assess models efficiently, or alternatively to develop general-purpose, and necessarily complex, occasionally multidimensional codes using as building blocks the detailed mechanistic representation of the phenomena. The latter are applicable to many kinds of transients and usually employ a two-fluid model. Of course, very clear demarcations between the two trends do not exist and both developments often end up somewhere in the grey area in between. Differences related to the number of dimensions considered by a code are further discussed in Section 2.3.

Another difference in approach stems from the degree of specialization of the code. For example, JAERI has developed models and the REFLA code which are specialized for reflooding. The code thus contains the specific knowledge about reflooding phenomena that is in hand. As a consequence of its specialization, the code is fast running; it can be used to assess models using large amounts of data. Such models can then be inserted in more sophisticated codes. Studies of the response of the model for a particular phenomenon can be made by providing measured data to the code and examining the prediction of the remaining information by that particular model.

The opposite trend, the most general approach, addressing in detail all phenomena, has been implemented in codes such as RELAP5 and TRAC; the pieces of information needed to build such a code are continuously provided by researchers attempting to further improve them (e.g. Nelson and Unal, 1992; Yadigaroglu and Andreani, 1989).

Comparison of the partial models and correlations needed for these two approaches is not straightforward and cannot be conducted independently from the framework within which they have been developed. The analyst should recognize the differences in approach, and determine which one is the best
for addressing his problem.

The REFLA code developed at JAERI mentioned above (Murai et al., 1984) is discussed here as an example of the category of specialized, fast running, one-dimensional codes, based on phenomenological understanding. In the JAERI model, the experimental knowledge regarding the physics of the reflooding process available from many JAERI experiments and from analysis of other laboratories' data is built into the model. Some parameters were determined on the basis of experimental data already available more than ten years ago.

To introduce considerable conservatism into the code for licensing purposes, REFLA's heat transfer coefficients (h.t.c.) in the post-CHF region were multiplied by a factor of 0.1 and the code was coupled with RELAP4. On the other hand, to produce a more generally usable code, the one-dimensional REFLA core model has been re-written using the two-fluid formulation and installed in TRAC-PFI; the resulting REFLA/TRAC code (J-TRAC) has been used as a best-estimate reactor transient analysis code. The fast running nature of REFLA/TRAC is due to good coupling of hydrodynamic and heat transfer correlations and the use of quench front (QF) velocity correlations instead of detailed consideration of the phenomena at the QF (Akinoto et al., 1988).

In order to develop REFLA/TRAC, numerous bundle reflood tests with 15x15 rod fuel assemblies were performed at JAERI (Murai and Inoguchi, 1982; Murao and Sugimoto, 1981). The code predicted well JAERI's bundle tests, many FLECHT tests, SEPFLEX tests and large scale CCTF and SCTR tests (Murao et al., 1984; Okubo and Murao, 1985). In assessment calculations, effects of clad material, gap conductance, grid spacer, mixing vanes, and rod spacing were investigated including tests with 17x17 rod bundles and tight lattice fuel assemblies; most of these had used Inconel-clad, indirectly heated rods. Zirconium-clad heating rods with a gap simulating the actual one between the clad and the fuel pellet were also included, however.

The TRAC code developed by the Los Alamos National Laboratory, under the sponsorship of the US NRC, is used as an example of the more complex multidimensional code. A recent version TRAC-PFI/MOD2 of this code includes a new, more phenomenological model of the thermal-hydraulics of the reflooding process (Nelson and Unal, 1992; Unal and Nelson, 1992). The choice of all closure relationships is based on a single post-CHF flow regime map suggested by Ishii and his coworkers (Ishii and DeJarlais, 1986; 1987; Obot and Ishii, 1988); this assures the proper interrelationship between the closure quantities. Wherever possible, correlations known to apply to a given flow regime for a particular closure quantity were used. However, the original correlations frequently could not be applied directly but had to be modified. For those cases, Nelson and Unal used the "kernel" or "functional" dependence of the original correlation and modified only its magnitude by use of a multiplier. When no correlations were available, weighting functions interpolating between relations applicable to known bounding regimes were used. To a certain degree, the number of these "weighting functions" or "ad hoc corrections" required is a reflection of the current state of the art.

The remainder of the paper shall discuss the current state of the art for modeling reflood. The discussion will necessarily focus on the existing codes and their successes and shortcomings. The emphasis will be on the rather complex general-purpose codes, although specialized codes will also be occasionally considered. The discussion shall include modeling
approaches, correlation packages, and problems which currently still exist. The purpose is not to conduct an exhaustive review of all aspects of the problem, but rather to reach consensus on certain issues at least. This review, which attempts to be critical, necessarily addresses the shortcomings rather than the well known successes of the codes.

2 SUCCESSES AND DIFFICULTIES IN MODELING

2.1 Modeling of Two-Phase Flows — The Two-Fluid Model

Our current reflow analysis capability is built around what is called the two-fluid model. This model represents the vapor and liquid phases using a set of field equations (mass, momentum, and energy) for each phase. The resulting "two fluids" are coupled together by closure relationships including the equations of state, interfacial drag and heat transfer, and wall drag and heat transfer. Mass transfer between phases is obtained from the interfacial heat transfer relationship (which is a simple thermal-energy jump condition) and the saturated-interface condition. Frequently, flow regime maps are used to define when flow regime transitions occur so that the model knows which of the available closure relationships should be applied.

The two-fluid approach allows both velocity differences and thermal nonequilibrium between the phases to develop naturally. As such, it can, for example, more realistically represent both the injection of subcooled ECC water into steam, reflow, and other phenomena or accidental situations where large departures from the equal-velocity, equal-temperature conditions between phases exist.

2.2 Number of Fields

One obvious limitation of the two-fluid model is that it represents only two fields. In reflowing, there are several situations where this limitation is detrimental. Some of these limitations are insignificant while others are not.

For example, the introduction of a noncondensible gas from ECC injection produces a two-component gas system, with the momentum and energy equations necessarily representing the gas mixture. The assumption of mechanical and thermal equilibrium between the two components of the gas is a good approximation. Certain closure relationships (e.g. interfacial heat transfer) may, however, be affected by the noncondensables (which are not considered explicitly) and this may have a significant effect on mass transfer.

Another example frequently occurring in reflow is when liquid drops are carried up by the vapor while a liquid film runs back down cold walls (e.g. the core barrel and control rods) within the core; right side of Fig. 1. Here, the equal velocity condition for the two liquid fields (droplets and film) is certainly not realistic, but it may affect only the limited upper portion of the core. The liquid film established on the cold surfaces, adds significantly to the liquid fraction, especially near the top of the core where the liquid fraction is low. In a typical PWR core the unheated surface of the control rods alone is approximately equal to 10% of the
total rod heat transfer area. For liquid film thicknesses of 0.2 to 3.0 mm and typical reactor hardware, one can calculate film volumetric liquid fractions between 0.6 and 11%. Thus, at high void fractions, the liquid contained in these films cannot be neglected. A more detailed discussion of these issues is given in Section 3.4.2.

Williams (1985) has shown that the vapor velocities generated in the core during reflood can cause countercurrent flow limitations, resulting in a "hanging" or falling liquid film condition. Both the overall drag coefficient and the wall heat transfer must consider both the droplets and the film. It is clearly not possible to do this directly with the two-fluid approximation since only one liquid field is assumed.

Two-liquid-field codes such as COBRA-TF (Thurgood et al., 1980; Kelly, 1979) and W-COBRA/TRAC (Thurgood et al., 1982) offer superior modeling potential in this respect, at the expense of increasing the number of partly unknown closure laws.

Another example of limitation due to the number of fields is inverted slug flow, where some of the steam flows as a superheated vapor film near the wall and some as part of the saturated two-phase core mixture, with significantly different velocities. Again representation of the two steam fields by a single fluid is not possible.

\[
G_r \cdot \eta G_f = G_f' \\
G_f' = \frac{G_f}{1 - \eta}
\]

Fig. 1. Schematic of two situations where a single-fluid representation of the liquid is not sufficient. Left: recirculation patterns created by droplets in the core. Right: falling liquid film and rising droplets.
2.3 Multi-Dimensional Effects

The major findings of reflooding experiments conducted in large-scale system-effects facilities and tests conducted with the specific purpose of studying the effects of multidimensional flow and power distributions in the core are discussed in Section 3.3. These experiments have shown that multidimensional effects do exist; their influence on heat transfer during reflooding is usually rather beneficial. Most codes have difficulties in representing these effects; they treat the core as one-dimensional (or at most as a few parallel 1-D channels) without real modeling of any cross-flows.

The difficulties of 1-D codes are rather obvious. The multidimensional codes have a totally different set of problems. First, these codes generally assume that the flows are not controlled by frictional effects, while wall drag dominates the flows across multiple channels. Apart from this restriction, the field equations are in principle capable of representing any multidimensional flow pattern resulting from numerous other driving forces. The problem comes in properly representing the change in flow characteristics as the flow changes from predominantly axial along the fuel rods to cross-bundle (transversal) flow. Frequently, the same closure relationships are utilized in all flow directions (axial, radial, or circumferential) even though liquid entrainment and heat transfer are known to be different under axial and cross-flow conditions.

The loss coefficients used for cross-flow in rod bundles are usually based on single-phase flow data and are often simply a constant value (e.g., 0.5 times the number of rows of rods between the centroids of the hydrodynamic cells). For two-phase flow conditions, these coefficients should be adapted and, perhaps more importantly, should be different for the liquid and vapor phases. In bubbly flow, the bubbles tend to follow the rods (axially) and resist being squeezed between the rod gaps. Similarly, during dispersed flow film boiling, it is difficult to imagine any but the smallest drops managing to pass between two rods that are literally "red hot." Such physical insights are difficult to include in the 3-D closure laws.

2.4 Problems with Space and Time Averaging

The field equations for the two-fluid models used in system codes are volume- and time-averaged. A detailed discussion of the effects of spatial and time averaging of the closure relationships can be found in Nelson and Pamasenetoglu (1992).

Typical node sizes used in modern codes imply that the volume averaging is done over many fuel rods even in the finest hydraulic noding schemes, which is (for system analyses, at least) employ nodes of the order of $0.1 \, \text{m}^3$.

Since the closure relationships are obtained from steady-state or quasi-steady experiments with data reduction procedures which rely on the stationary nature of these data, the "quasi-steady" assumption is inherently present in practically all closure relationships. Thus temporal averaging effects are present via both this quasi-steady assumption "hidden" in the closure relationships and overtly through the temporal-averaging operator used to arrive at the set of field equations used.

From the temporal standpoint, one must consider the relationship between:

- the time-step dictated by the code, $\Delta t$. 

- the time scale inherent in the physical phenomena $\tau_c$ (e.g. slug passage times),
- the integration period used for temporal averaging of the conservation equations, and
- the time scale (or constant) of the transient $\tau$.

Nelson (1986, p. 1133) noted that the minimum averaging time $\delta t_{\text{min}}$ must be large enough to include a sufficient number of events/cycles of the governing phenomena (characterized by $\tau_c$). Interesting questions are raised regarding the closure relationships when $\delta t < \delta t_{\text{min}}$ or $\delta t > \tau$.

In the past, the second question, $\delta t \tau$, was not important since the $\delta t$ of the calculations was controlled frequently by the material Courant limit. However, as time-steps continue to increase with improved numerics, this issue becomes also relevant. Detailed examination of these questions (Nelson, 1986) is beyond the scope of this paper. In practice, there are no truly transient closure models in use and such developments are not practical, for the time being at least.

Regarding spatial averaging, Nelson (1986, p. 1129-1132) discusses its influence upon the wall-to-fluid heat transfer. An area-average often enters the data reduction procedure when steady-state experiments are analyzed; for example, the data necessarily include the effect of the area-averaging arising from thermocouple spacing and size. Heat transfer experiments with a progressing quench front inherently involve area averaging. Near the quench front, steep gradients of the wall temperature are frequently encountered. If a closure relation is developed including a "history effect," for example dependence on the distance from the quench front (see Section 5), one must make sure that such a law is used under conditions (axial wall temperature gradient $dT_w/dz$) similar to those of the data upon which it was based.

3 HYDRODYNAMICS OF REFLOODING AND SYSTEM EFFECTS

The series of tube and bundle experiments conducted in the seventies that allowed identification of trends and established the understanding of the reflooding heat transfer and two-phase flow phenomena were reviewed by Yadigaroglu (1978). Many additional such experiments were performed since. Several large-scale system-effects tests conducted later in Germany and in Japan provided valuable insights into multidimensional effects taking place in the core and system-core interactions. As a result of all these experimental programs a very large experimental data base is available today. Figure 2 identifies the various reflooding phenomena in the core and the pressure vessel.
Fig. 2. Refill/Reflood phenomena during a PWR cold-leg large break.
(CSNI, 1989)
3.1 Separate-Effects Tube and Bundle Reflooding Experiments

The most extensive series of rod-bundle experiments were conducted in the United States (the various FLECHT series - see for example one of the later reports by Lilly, 1977; and the FLECHT SEASET series - see Lee et al., 1982); in Germany - the PKL-I (Mayinger et al. 1977), FEBA (Igle et al., 1984a) and SEFLEX (Igle and Rust, 1985) tests; in France - the ERSEC, PERICLES (Deruez et al., 1984) and BETHSY experiments conducted at the CENG, in Grenoble); in Japan (Mura et al., 1985; Okubo and Mura, 1985), and elsewhere.

The reflooding experiments with tubes and bundles established the fact that two distinct sequences of flow regimes are possible. The deciding factor is the void fraction (linked of course to the local subcooling/flow quality) at the GF (Yadigaroglu and Yu, 1983) which is, in turn, related to the reflooding rate, inlet subcooling, and power level. When the flow at the GF is subcooled, Inverted-Annular Film Boiling (IAFB) takes place immediately downstream of the GF. At low pressure, as soon as the flow at the GF becomes saturated, the void fraction increases significantly and the flow ends up being highly dispersed. In this case one speaks of Dispersed-Flow Film Boiling (DFFB). As an intermediate situation, an Inverted-Slug Film Boiling (ISFB) regime may appear.

When the void fraction at the GF is high, a climbing-film (annular) flow regime prevails just below the GF. Sputtering at the GF removes this film from the wall and DFFB is immediately established. The differences between these sequences of regimes have been displayed in detail by Yadigaroglu (1991).

Although the existence of these regimes has been well known for a long time, more recent findings about the actual accident scenario tend to give less importance to the highly subcooled cases, since various system factors discussed below tend to bring the coolant nearer to saturation at the bottom of the core. The CCTF and SCFC tests indicate that ISFB occurs during the major part of the reflood transient.

Figures 3 and 4 from later UC-Berkeley tube experiments (Ng and Banerjee, 1983; Kukona et al., 1983) show typical behavior of the wall temperature and of the void fraction for a "subcooled" and a "saturated" run. The wall temperature decreases rapidly in the IAFB region (of the subcooled run) where the heat transfer coefficient is relatively high and the void fraction low. For the saturated run, the temperatures at the higher elevations tend to remain relatively constant and the void fraction is near unity. Differences between flow behavior in tubes and in bundles will be discussed later in Section 3.6.

3.2 System Effects

The controlled-flow-rate bottom-reflooding experiments conducted to understand cold leg injection do not generally account for the system effects. These experiments were very useful for model development, validation of the thermal-hydraulic modeling of reflooding, and code assessment. They can give, however, an erroneous or at least incomplete view of the effects of important system factors on the maximum clad temperature or the core rewetting time. Single-bundle reflooding tests where the entire primary system behavior was also simulated were conducted in several countries (e.g. the FLECHT-SEASET (Lee et al., 1982), PKL-II in
In forced-feed versus gravity-driven reflooding, the heat removal in the core and the reflooding rate do not have the same relationship. In the former case, it is the flow rate that conditions the power exchanged, while in the latter the reflooding rate is dependent from the heat release (steam binding effect). Indeed, the differential pressure between the upper plenum and the top of the downcomer is equal to the gravity head difference between downcomer and core; it is limited by the value corresponding to a water level in the downcomer at the elevation of the cold legs. This differential pressure is also equal to the frictional pressure loss of the steam or mist flow in the loops. The steam flowing in the loops is mainly the result of the heat release from the rods. A fraction of this steam is also produced outside of the core by vaporization of the entrained liquid through heating by the hot walls in the upper plenum, hot legs, and steam generators (3G), or by vapor desuperheating. In summary, the core cooling rate is limited by the height of the downcomer which determines the maximum head available for reflooding the core.

Reliability of code calculations depends strongly on the correct prediction of the response of the entire system. The most important system considerations are discussed in the rest of this section.

Fig. 3. Variation of the wall temperature and of the void fraction for a "subcooled run" (Ng and Banerjee, 1983)
Fig. 4. Variation of the wall temperature and of the void fraction for a "saturated run" (Ng and Banerjee, 1983).

The amount of accumulator water not bypassed to the break and available in the lower plenum at the beginning of reflooding is determined during the refill phase of a LB LOCA. The condensation rate during this phase controls also the liquid subcooling at the inlet of the core. Excessive bypass to the break will result in a reduced reflooding rate as long as the countercurrent steam flow in the downcomer has not dropped sufficiently to allow major penetration of ECC water to the lower plenum. Underestimation of the condensation produces too high subcooling in the lower plenum; the collapsed water level in the core is increased. This is unfavorable, as the water head difference between downcomer and core is reduced. On the other hand, the occurrence of an inverse annular flow pattern above the GF will persist longer; during this phase the liquid carry-over is very low; this is favorable regarding the amount of steam that can be produced in the core. But as the liquid carry-over is low, heat transfer far downstream from the GF is not so efficient. Underprediction of the subcooling in the lower plenum will produce earlier boiling of the water in the downcomer due to reactor vessel wall heat release limiting the downcomer head, and will also prolong the bypass phase. All these effects demonstrate that the entire reflooding transient prediction depends a lot on a reliable calculation of the refill phase.

Present thermal-hydraulic codes have some difficulties in calculating correctly condensation rates during the refill phase because of the very unstable nature of the flow (observed in many experiments such as UPTF and LOFT). High condensation rates and the resulting unstable flows present a most serious challenge to the quasi-steady-state assumption inherent in all
closure laws (see Section 2.4). They naturally lead to the question of how fast should the closure laws change. High condensation rates can drive the time increment to its lower limits in most numerical schemes if instantaneous changes in the coefficients provided by the closure laws are allowed (quasi-steady assumption); this area deserves more investigation.

Steam binding represents the flow resistance between core and break. This pressure loss is mainly due to wall friction in the SG tubes and in the pumps. There is also an acceleration pressure drop in the SG's due to evaporating drops and superheating of the vapor. This total pressure loss is limited by the available downcomer head, as already mentioned above. In the most favorable case, all the vapor flowing in the loops is created in the core and participates to rod cooling. As more liquid droplets vaporize in the SG, less vapor can flow out of the core. Thus it is very important to predict correctly the amount of water entrained to the SG's.

Some weaknesses of the codes in this area may be suspected since the de-entrainment on the structures of the upper plenum is difficult to predict. Observations in the ACHILLES test facility have shown that the droplet spectrum out of the core is very wide and one can expect that the smallest drops follow the steam up to the SG, while the largest ones are de-entrained in the upper plenum. Present system codes cannot deal with such a partition as they generally describe what occurs for an average size drop only. On the other hand the SCTF and UPTF tests with upper plenum injection have shown that when a pool builds up in the upper plenum, the amount of liquid entrained into the hot legs depends on the height of this pool; the droplet spectrum at the exit of the core is no longer relevant.

Oscillatory reflooding was observed in system test facilities (PFL, BETHSY) at least at the beginning of the transient. When water from the accumulator enters at the bottom of the core, the important vapor generation taking place in the core initiates flow oscillations between the downcomer and the core with a large amplitude (± 1 m/s in velocity). The amplitude and duration of these oscillations influence greatly the length of the entire reflooding phase.

The maximum amplitude of the oscillations controls the quantity of coolant lost at the break and the average downcomer level; these are key parameters limiting the efficiency of core cooling. At each oscillation cycle some water is entrained out of the core. This water will vaporize in the upper plenum by heat release from the hot structures or in the steam generator and will produce very unfavorable steam binding. Moreover all this water is lost and reduces the downcomer level which is the driving force. The duration of this oscillatory phase is thus a critical feature.

Analysis of code predictions of such oscillatory behavior observed in BETHSY reflooding tests, suggests that the interfacial friction downstream of the QF controls the quantity of water entrained. The vaporization of this water downstream of the QF and outside of the core induces the flow reversal, and excites the oscillations. A fine mechanical description of the flow downstream from the QF is thus required, particularly in case of inverse annular flow which may take place during oscillations at the beginning of reflooding. Forced feed experiments with rod bundles do not provide sufficiently detailed information in this case. Attention should be focused to the few experiments showing flow oscillations (see e.g. Clement et al., 1982) in order to assess reflood models under realistic conditions. (Forced oscillatory tube reflooding tests are described by Kawaji et al., 1985 and Oh et al., 1986).
It must also be noted that the numerical aspects of the solution have an influence in oscillatory reflooding. Numerical schemes which are diffusive may damp the oscillations too much; time and meshing convergence tests are necessary. On the other hand, many reflood codes use a fine mesh for heat transfer from the rods in the vicinity of the QF together with a coarser hydraulic meshing. Some perturbations of the hydraulics are induced each time the QF progresses from one hydraulic mesh to the following. Attention must be paid to minimize these perturbations in order to avoid numerically induced oscillations.

The three classes of main system effects mentioned above must be kept in mind when reviewing reflood modeling capabilities. If these effects are ignored, inappropriate conclusions may be drawn from constant forced-feed reflood tests. For example, if one compares tests performed under identical reflood conditions using either conventional rods or more realistic rods incorporating a gas gap, large differences are observed (Ihle et al., 1984b). The peak cladding temperature is reduced and the QF velocity is higher when rods with a gas are used. Extrapolating to gravity-driven reflood conditions, one must consider, however, that the total heat removal in the core is limited by system effects, as noted above. Conclusions on the conservatism of one rod design compared to the other become difficult.

Regarding oscillatory reflooding again, another conclusion can be drawn. It was shown that large-amplitude oscillations were unfavorable since they were reducing the downcomer head and enhancing steam binding. It is also presumed that a high heat flux downstream from the QF may induce a high vaporization of the entrained water; oscillations are excited and may persist. This fact disqualifies the conservative approach which minimizes heat transfer coefficients since it can also lead to underestimation of the vaporization of the entrained water and its unfavorable effects on the oscillations. Progress is only to be expected from the best-estimate approach, and it is only very good BE codes that may definitely establish that an assumption is indeed conservative. In this respect, further efforts should be made in system code developments for an accurate modeling and an extensive assessment of the following main points:

- correct refill phase prediction for producing good initial and boundary conditions for core reflood, considering the difficulties related to the high condensation rates.
- good prediction of the steam binding effect including the complex conditions of water entrainment and deposition downstream of the core.
- prediction of the oscillatory behavior and the conditions that lead to its artificial damping or amplification.

3.3 Experience Gained from Multi-Dimensional System-Effects Tests

Large scale system-effects tests have substantially increased our knowledge about fluid dynamics in a reactor under realistic conditions. Such tests were conducted in facilities like UPTF (Weiss, 1989), SCRF (Adachi et al., 1983; Iguishi et al. 1988; Inamura et al., 1989), CCTF (Murao et al., 1982), and PFL-II (Handl et al., 1985).

Major findings with impact on reflood modeling are (see Mayinger, 1989):

- A considerable part of the core rewets already during depressurization, as PFL-II combined-injection tests have shown (Handl
et al., 1985). In other facilities, e.g. CCTF, this effect occurred also under other modes of injection.

- Full scale downcomer experiments have shown that the ECC water injected into the cold legs condenses large masses of steam in the pipes and in the downcomer, thereby losing a large fraction of its original subcooling before reaching the bottom of the core.

- There is an asymmetrical "penetration" of ECC water in the downcomer during cold leg injection, depending on the distance of the injecting loops from the broken loop. All ECC water injected near the broken leg is directly bypassed, however, to the break. Due to this heterogeneous behavior, increased water delivery rates were observed at full scale relative to previous tests in subscale facilities (Glaeser, 1992).

- Condensation induced oscillations of loop flow lead to oscillating core flows during the accumulator injection period.

- ECC water injected into the hot legs leads to the formation of two distinct cooling regions in the core:
  - a break-through region with subcooled water downflow: the bundles near the ECC injection points (Mayinger et al., 1992)
  - a bottom reflood region with enhanced two-phase upflow due to circulation induced by the downflow of subcooled water.

These observations, together with those from a number of separate-effects tests, have led to a partly revised view of reflooding boundary conditions and strongly suggest consideration of the following facts in modeling reflood:

- In the presence of low subcooling at the core inlet and highly oscillating flows during bottom reflood, the IAEB regime is not dominating.

- Under certain combined high-pressure injection conditions, the upper (top-down) QF can contribute just as much to the total quenching of the core as the lower QF; with bottom reflooding only its contribution is lesser.

- Rods can be quenched over their entire length without precooling by break-through of a sufficient amount of subcooled water from the top.

- Multidimensional effects in the core enhance heat transfer by supplying more water to the high-powered bundles below the QF and more entrained water in the liquid deficient region.

- To address the multidimensional effects observed in the core, especially when combined injection takes place, a good Counter-Current Flow Limiting condition (CCFL) model is needed.

- Two-dimensional modeling of the downcomer is necessary to properly address the ECC water penetration phase.

Additional findings can be derived from the PERICLES 2-D Reflood tests (Vettese et al., 1984; Kelly and Reinhardt, 1989), where the flow distribution effects in the presence of hot and cold bundles were investigated. In these tests, the boundary conditions imposed provided for approximately equal collapsed liquid levels in both the hot and cold assemblies. The major findings are:

- The froth front (inferred from the quench times of guide tubes and
from changes in the heat transfer regime) is somewhat higher in the hot assemblies.

- Liquid carryover is considerably higher in high power assemblies.
- Below the QF, liquid cross-flow occurs from the cold assemblies to the hot ones.
- In the DFFB region, vapor cross-flow occurs from the hot assemblies to the cold ones.

The liquid cross-flow is driven by the difference in gravity head due to the mismatch in QF elevations and the lower void fraction below the QF in the low power assemblies. Indeed, a very small pressure difference can drive a large liquid flow rate in the lateral direction. Combining the findings about equal overall collapsed liquid levels and different wetted-region gravity heads, one is lead to infer that more liquid must be suspended above the QF in the hot assemblies to make up the difference. These effects lead to reduced quench times for the hot assemblies as compared to the predictions of a 1-D hot channel analysis (Kelly et al., 1989).

Above the QF, the elevated wall temperatures and higher level of entrainment in the high power assemblies result in a higher vapor generation rate from droplet evaporation. The vapor frictional pressure drop in the DFFB region then drives a vapor cross-flow from the hot channels to the cooler ones. This effect reduces heat transfer in the DFFB region of the hot channel and can offset the beneficial effects described just above, leading to a peak cladding temperature in excess of the 1-D hot channel calculation (Kelly et al., 1989).

Similar insights were obtained in the SCTF tests. Analysis of these tests suggests that some part of the liquid is conveyed by the steam from the QF regions of high-power bundles to the QF regions of low power bundles and falls down, resulting in a large flow circulation pattern in the core. To explain the observed enhancements in heat transfer, high recirculating liquid mass flow rates above the QF are necessary. Radial differences in accumulation of water drive upper plenum and core circulation.

Another less positive finding is that at the beginning of the reflood period, as the liquid level in the core starts rising, hot water comes to the liquid surface, apparently due to buoyancy effects. Thus hotter than expected water produces earlier initiation of boiling in the channel and earlier core cooling than in the standard calculations.

3.4 Current Modeling of Core Hydrodynamics

Modeling of the hydrodynamics of reflooding is usually associated to a way of representing the two main post-CHF flow regime sequences mentioned above. Examples of this representation in the specialized, as well as the more complex general-purpose codes will be briefly discussed below.

3.4.1 Void Fraction Below the QF

The pressure drop in the wetted region of the core is a very important system response consideration since it determines the main part of the differential pressure Ap across the core, the Ap above the QF being relatively small. The core Ap is balancing the downcomer and loop Ap’s and influencing the core inlet mass flow rate, as discussed in Section 3.2.
The potential for liquid cross flow below the QF mentioned in Section 3.3, coupled with the fact that modeling of this wetted region also determines the conditions at the QF, gives it additional importance. Cross flow below the QF is important for two reasons: First, it adds liquid to that coming from the lower plenum; potential recirculation within the core and upper-plenum provides liquid which may have precursory cooling effects that are neglected if only mass balance information from what enters and leaves the vessel is used. Secondly, this recirculated liquid is typically saturated and when mixed with the incoming subcooled liquid produces a higher temperature mixture.

The conditions below the QF dictate the phase velocities and the cross-sectional phase distribution in the QF region and therefore determine the type of flow regime that exists above the QF. The importance of correctly predicting interfacial drag below the QF in relation to heat transfer calculations in the post-dryout region was also emphasized by Akao et al. (1987) and Analytis et al. (1987) in relation to both reflowing and boiloff predictions.

In recent work, Kelly noticed significant discrepancies in void fraction predictions, both in bubbly and annular flows during reflow. For bubbly flow, it appears that at low vapor superficial velocities (of the order of 0.2-0.4 m/s) and low liquid mass fluxes (liquid velocities of the order of 1-5 cm/s), the bubbles concentrate near the heated surface and rise, entraining near-wall liquid with them; this liquid may even rise to the top of the bundle and then fall back in the center of the channel. Simply put, the void and velocity profile distributions are different than in "normal" higher volumetric flux cases. The resulting higher slip is evident over distances of 0.2 to 0.4 m only, but under gravity-dominated pressure drop conditions, it can affect the bundle pressure drop by 5 to 10%. There is no direct experimental confirmation of this, but it was inferred from an examination of both bundle and tube void fraction data.

For annular flow (at void fractions > 80%), Kelly, in agreement with similar conclusions drawn by Abe et al. (1991), notes that the usual Wallis (1969) correlation greatly underestimates the interfacial friction. It appears that for low flooding rates, the effect of gravity upon the velocity profile within the liquid film becomes important. Often, the wall shear is very low or even negative despite the overall upward nature of the flow. Thus, at the interface, the liquid moves up dragged along by the vapor, while next to the wall it may fall back. It is supposed that this "counter-current" flow situation within the films significantly enhances the interfacial waves and hence the apparent roughness. This conclusion is based on an analysis of tube data where both void and pressure drop measurements were made (see also Section 3.6).

The experiences of various investigators related above point to the need for a more realistic modeling of the hydrodynamics below the QF, such as the one described below.

Mechanistic Modeling. The latest version of TRAC mentioned above (Nelson and Ural, 1992) divides the boiling region below the QF into three parts, depending on the void fraction. Below a void fraction of 0.5, partial or fully-developed subcooled boiling, and/or fully-developed saturated nucleate boiling can occur. For void fractions greater than 0.98, an annular-mist region is assumed to exist. Between these two regimes, a slug
region is assumed.

The drag in the low-void region is made up of two parts. One due to bubbles near or on the wall, and the other due to bubbles in the free stream. Near the wall, the model uses the drag of a rough pipe where the roughness is due to the bubbles. The free-stream drag is obtained from the expression given for bubbly flows by Iaihi (1987). The annular-mist interfacial drag coefficient is calculated as proposed by Sore et al. (1992). For void fractions greater than 0.5 but less than 0.98, a certain weighting is applied (for details, see Nelson and Unal, 1992).

Phenomenological-Empirical Modeling. JAERI obtained good results for the void fraction below the QF with the Murao-Iguchi correlation (Murao, 1979). The Murao-Iguchi correlation considers also the liquid velocity and is corrected for the higher void fractions. The same correlation is applied at JAERI to the ISFB region.

3.4.2 Flow Regimes Above the QF

The recent modeling of the reflood process in TRAC-PFI/MOD2 (Nelson and Unal, 1992) is used to introduce this item here. The work is based on the post-dryout flow regime map of Iaihi and his coworkers (Iaihi and Djarlais, 1986, 1987; Obot and Iaihi, 1988). One unique feature of this implementation is that both hydrodynamic and heat transfer closure sets are interrelated.

Table 1 shows Iaihi’s proposal and the TRAC implementation. Additional constraints on the original Iaihi map were implemented in the code because transient calculations produce situations not present in the steady-state experiments used for the original model development. For example, the early part of a prediction with liquid flowing into an empty tube may, based on the original correlations alone, indicate the presence of certain flow-regimes downstream at locations where the liquid had not yet had time to move.

The model, which is built around the IAFB flow structure, produces the other flow regimes when portions of the IAFB structure collapse. For example, when the smooth and rough-wavy regimes collapse, the slug or agitated regimes replace them at the QF. If the smooth, rough-wavy and agitated regimes collapse, the dispersed flow regime begins at the QF. The model has the potential of also modeling top-down quench; this feature has not yet been implemented, however.

Mechanistic Modeling of Hydrodynamics. Based on the flow regime map proposed of Iaihi and coworkers, the latest version of TRAC (Nelson and Unal, 1992) divides the post-dryout region into the following flow regimes: transition boiling, smooth IAFB, rough-wavy IAFB, agitated IAFB (inverted slug), post agitated, and highly dispersed. The transition boiling and the last two regimes were not present in Iaihi’s original map. Indeed, in his experiments, the flow regions began with the smooth IAFB region due to the particular techniques used. Thus, the regimes defined by Iaihi’s model are applied downstream of the end point of transition boiling. The highly dispersed region was defined as beginning at a void fraction of 0.98. The basis for the drag coefficients in these regions is indicated in Table 2 below. Nelson and Unal (1992) provide the details.
Table 1
Post-CHF Flow Regime Transition Criteria
(Nelson and Unal, 1992)

<table>
<thead>
<tr>
<th>Regime</th>
<th>Ishii's Corr.</th>
<th>Implementation in TRAC</th>
</tr>
</thead>
<tbody>
<tr>
<td>Smooth IAF</td>
<td>$Z = 60 \text{Ca}^{1/2}$</td>
<td>$Z = 60 \text{Ca}^{1/2}$</td>
</tr>
<tr>
<td>Rough-Wavy IAF</td>
<td>$Z = 295 \text{Ca}^{1/2}$</td>
<td>$Z = 295 \text{Ca}^{1/2}$</td>
</tr>
<tr>
<td>Agitated IAF</td>
<td>$Z = 595 \text{Ca}^{1/2}$</td>
<td>$Z = 595 \text{Ca}^{1/2}$</td>
</tr>
<tr>
<td>Dispersed IAF</td>
<td></td>
<td></td>
</tr>
<tr>
<td>(post-agitated IAF)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Highly dispersed IAF</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 2. Basis for Interfacial Drag Coefficient Selection in TRAC/MOD2

<table>
<thead>
<tr>
<th>Region</th>
<th>Basis</th>
</tr>
</thead>
<tbody>
<tr>
<td>transition boiling</td>
<td>same as nucleate boiling</td>
</tr>
<tr>
<td>smooth IAFB</td>
<td>smooth tube, laminar and turbulent</td>
</tr>
<tr>
<td>rough-wavy IAFB</td>
<td>same as smooth but with roughness</td>
</tr>
<tr>
<td>agitated IAFB</td>
<td>same as rough-wavy IAFB</td>
</tr>
<tr>
<td>post-agitated dispersed</td>
<td>void weighted based on agitated and dispersed-regime coefficients</td>
</tr>
<tr>
<td>highly dispersed</td>
<td>droplet drag based on drop size Ishii (1989)</td>
</tr>
</tbody>
</table>

When multiple regimes occur in a given hydrodynamic cell of the core, the interfacial drag models are length averaged to determine the cell average drag.
Phenomenological–Empirical Modeling. Rapid spreading of the droplets to the top of the core was observed in the CCTF and SCTF tests at JAERI. Figure 5 confirms this by displaying the traces obtained from the differential pressure transducers along the core. The indications of the Ap cells started increasing right after initiation of core reflooding, even at the highest elevations just below the tie plate. Simultaneously, bottom-down quenching occurred at the top of the core where the temperatures were low, confirming the fact that a significant amount of water arrived there rapidly.

![Graph showing differential pressure drop measurements](image)

Fig. 5. Differential pressure drop measurements obtained along the core in the CCTF tests. The hatched areas signal the passage of the QF.

The indications of the Ap cells show the prevalence of quasi-steady-state conditions, followed by an increase due to agglomeration of the droplets and formation of "inverted" slugs. When the QF arrives at the bottom of a measuring station, the Ap values start increasing; the increase continues until the QF "leaves" the region covered by that particular Ap cell (hatched areas in Fig. 5). A second quasi-state-state period is observed after the passage of the QF, corresponding to the presence of normal slug
flow below the QF.

In the FLECHT low-flooding rate tests (reflooding velocities less than 2.5 cm/s) the formation of inverted slugs was limited to a region about 0.3m above the QF. At higher reflooding rates, behavior similar to that of CCTF was observed.

This experimental evidence led to the development of the model depicted in Fig. 6. Case 2 corresponds to the CCTF findings, while Case 1 refers to the FLECHT low-flooding rate situation. The model is based on Weber numbers for the droplets calculated using the observed steam velocity at their point of entrainment and their observed diameter. The time constant for agglomeration of the smaller droplets into slugs was obtained from a separate small-scale reflood test.

As shown in Fig. 1 (right side), the droplets cool and rewet the flow housing (present in the FLECHT tests). Droplets can then be collected by the falling liquid film and a recirculation pattern is established, affecting the velocity of the liquid phase. The equivalent upwards velocity of the liquid is doubled if 50% of the droplets are captured by the liquid film; this increases the effective reflooding velocity in this part of the channel. Therefore, as the housing is cooled down, the situation labeled Case 2 in Fig. 6 appears. The reasons for appearance of Case 2 in the CCTF tests must be related to the presence of numerous non heated rods and to the opening ratio of the tie plate.

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![Diagram of droplet distribution](image)

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Fig. 6. Modeling of the distribution of the droplets above the QF.
Case 1: low flooding rate.  Case 2: higher flooding rate.
3.5 Droplet Hydrodynamics

Droplet hydrodynamics are very important for the correct modeling of post-dryout heat transfer with dispersed flows. To model droplet hydrodynamics, one must know the droplet size distribution (or a representative average), the entrained liquid fraction, and the droplet drag coefficient.

The drag coefficient can be reasonably well approximated (given the other uncertainties) by the relations for spheres. The problem of entrainment fraction and the combination of the interfacial shear due to drops and liquid slugs or a film (limitation due to the unique liquid field) has been mentioned above.

There is a difficulty in determining the onset and the fraction of liquid entrainment. Current approaches include:

- use of a modified pool entrainment correlation (e.g. in COBRA/TRAC)
- use of a void fraction criterion for determining the point of liquid entrainment (e.g. in CATHARE and TRAC-PFI): if \( \alpha > \alpha_{cr} \) all the liquid is treated as drops; there is of course a need to "emp" the entrainment rate.
- using a multi-group droplet spectrum, entrain the fraction of drops that have sizes that can be entrained (e.g. Kawaij and Banerjee, 1987; Andreani, 1992).

The droplet size is the last point discussed now. Both the concentration and the spectrum of the droplets are strongly dependent from their previous history and from the generation mechanisms, so that heat transfer in DPHB is clearly history-dependent. The codes can in principle consider the development of the flow. They lack, however, the detailed description of the droplet spectrum development mechanisms.

Normally, one assumes that the Sauter mean diameter provides an adequate representation of the droplet population. Modeling of all relevant phenomena and representation of the entire spectrum of droplet diameters by a single average value properly satisfying all averaging requirements is, however, not rigorously possible. The spectrum of droplet sizes may also vary from case to case. ARILLES data have shown a large size distribution (Dore and Dhuga, 1991).

There is experimental evidence showing that at a short distance from the droplet generation point, droplets of different sizes attain the same velocity. Thus consideration of groups of droplets of various sizes is made easier (Andreani, 1992), but there is lack of knowledge regarding their initial size distribution at the GF (Andreani and Yadigarolu, 1989) anyway. No system code currently models more than one droplet group; however, it is probably not necessary to add the complexity of multiple groups without simultaneously introducing droplet spectrum evolution models. Kawaij and Banerjee (1987) compared the predictions of a one-group model with those of a multi-group model and observed only minor differences. Spectrum development effects such as breakup of the droplets were not, however, considered.

Droplet models implemented into the codes usually describe the droplet population by a single mean diameter, as already mentioned; this diameter is determined from an aerodynamic stability criterion and the method can lead to physical inconsistencies. Indeed, in TRAC-PFI/MOD1, RELAP5/MOD2, and RELAP the average AXIAL droplet diameter is obtained from a criterion
based on the local Weber number

\[ \text{We} = \frac{\rho_g (\Delta u)^2 d}{\sigma} \]

where \( \Delta u \) is the phase velocity difference, \( \sigma \) the surface tension, and \( d \) the maximum stable droplet diameter.

First, from basic experiments, the critical Weber number for breakup is about 12-14, whereas the reflux entrainment models use values about 2 or 3. Second, droplet breakup does not occur instantaneously as the Weber number reaches its critical value. Third, and most important, near the point of creation of the drops \( \Delta u \) is large; as the droplets are accelerated by the steam flow, \( \Delta u \) diminishes and, according to this Weber number criterion, their diameter could grow, in variance with reality. Finally, fourth, there are only few examples where it was indeed important to model aerodynamic breakup during reflux; most of the time the steam velocities remain low, not justifying this type of breakup.

In fact, the characteristic mean droplet size should also be dependent on flow history and on the mechanism that has created the entrainment, as noted above. Codes that use an interfacial area transport or a number-density conservation equation (such as COBRA/TRAC and TRAC-BF1) could be superior to the codes that use local conditions to determine the droplet diameter and from there the interfacial area. Certain alternatives in defining the droplet diameter are reviewed below. No consensus on how to calculate the average droplet diameter exists.

COBRA/TRAC uses a Weber number criterion to define the droplet diameter at its entrainment point; after this, the axial evolution of the drop diameter is computed using an interfacial area transport equation. Processes such as evaporation, shattering due to grids, aerodynamic breakup, etc. are modeled as interfacial area source/sink terms.

COBRA-TF uses a correlation developed by Westinghouse for the BART code. Initial diameter is a function of pressure only (through saturation properties); an interfacial area transport equation is used thereafter.

TRAC-PF1/MOD2 uses a correlation developed by Ishii for pool entrainment. The diameter is a function of fluid properties.

CATHARE uses a correlation for the maximum drop size that can be entrained from a mixture. The droplet diameter is a function of pressure only. Also, a grid effect model, function of local drop velocity, is used that causes this drop diameter to be reduced as the drop traverses the bundle.

Most of these models predict a droplet size of about 1–1.5 mm, but their functional dependencies are different. They can even exhibit a different trend with pressure.

As we have seen, the drop size should depend on initial conditions and the evolution of the droplet spectrum should be calculated from a conservation equation. The choice of the local conditions that are determining droplet size is not, however, obvious. For example, is the local vapor velocity important? Or is the drop size more likely to be a function of the shattering that occurs when a liquid slug approaches the wall and is blown off by the vapor generation beneath it? Andramo (1992) produced a comprehensive and detailed model of droplet breakup by various mechanisms; tests of the impact of this model on DFFB heat transfer using a large
number of experimental data showed that the initial droplet conditions did vary consistently with flow conditions at the quench front.

3.6 Differences Between Tubes and Bundles

The first reflooding experiments were performed in heated pipes with a diameter equal to the hydraulic diameter of the core rod bundles. Most of the basic and fundamental understanding of the reflooding phenomena comes from these experiments. The simple geometry allows more detailed measurements than a rod bundle and precise void fraction data could also be obtained.

At the CENG, the thermalhydraulic modeling which was based on these experiments has been later tested and verified using rod bundle reflood data. Further developments were found necessary. Two differences were clearly identified concerning the interfacial friction in the rewetted zone and heat transfer above the QF in dispersed flows. Some effects of the duct geometry on the two-phase flow pattern were also observed (see Venkateswararao et al., 1982). These are briefly discussed below.

Interfacial Friction. Interfacial friction correlations specific to rod bundles have been developed using data from reflood tests, blowoff tests as well as blowdown tests. The main differences are observed for intermediate void fractions (0.2 to 0.8 approximately), when the size of the vapour bubbles is of the same order of magnitude as the characteristic dimension of the flow channel. The possible existence of vapour bubbles occupying several subchannels may be responsible for the relatively high drift velocity observed in rod bundles.

The differences with tube data are particularly important at the low pressures characteristic of reflooding conditions. The drift velocity of the bubbles had to be multiplied by roughly 10 going from pipes to bundles, corresponding to a reduction of the interfacial friction coefficient by a factor of 100 (Bestion, 1990). Similar conclusions were drawn by Aksan et al. (1987). The higher relative velocity observed in bundles is due to the different shape of the radial void and velocity distribution with respect to round tubes, an item already touched upon in Section 3.3. The drift-flux model used in the ATHLET code accounts for this by a higher phase distribution parameter $C_0$, verified by HETIS bundle tests (Sonnenburg, 1989). At present, all system codes have a specific set of interfacial friction correlations for rod bundles.

Influences on Heat Transfer in Dispersed Flow - Spacer Effects. Contrary to bubbly flows, dispersed flows may be less sensitive to channel geometry. The spacer grids always present in bundles are, however, affecting the droplet size distribution, from which heat transfer in DPFBR strongly depends (see Section 5.5) (Ille et al., 1983; Lee et al., 1983; Ille et al., 1984). Droplets can be shattered by hot spacer grids or captured and then released by rewetted grids. These effects result in a very broad size spectrum, as it was observed in the ACHILLES test facility (Dore and Dhuga, 1991).

Comparing tube and bundle data, one deduces that the spacer grids produce a significant reduction of the average drop size. For example, in a first version of the CANHARE code, a drop size model which was efficient in
Modeling of a Channel Undergoing Reflooding

Modeling of hydrodynamics and heat transfer under reflooding conditions is a challenging task; indeed, in addition to most of the well-established heat transfer modes and flow regimes, a number of rather specific situations that need special modeling appear. Hochreiter and Young (1985) have reviewed progress in modeling reflood heat transfer till around 1985. Not much fundamental progress has been accomplished since, in spite of the fact that certain shortcomings of the modeling are well recognized.

Classical reflooding takes place under low pressure and with very low mass fluxes (reflooding velocities of the order of 1 to 15 cm/s). Reflooding phenomena are on the average very slow. Imposing conservative EM conditions, reflooding a PWR bundle could take up to several hundred seconds; BE calculations result in core recovery times of the order of 100 s. There are no sudden pressure changes, and the average reflooding rates vary slowly. In reactor cores undergoing reflooding, flow oscillations due to system interactions are expected, however; these are superimposed on the slowly varying average reflooding rates. This understanding led to a somewhat artificial division of reflood situations into forced-feed and gravity-feed reflood. Forced-feed reflood experiments were designed to allow study of the phenomena independently from the system-induced oscillations. Assuming that the oscillatory processes do not affect the average trends strongly, one studies the fundamental reflood phenomena much easier via forced-feed experiments.

Considering these facts, one realizes that codes originally developed for calculating the (pressure difference driven) rapidly varying flow rates and pressures during the blowdown phase of the LOCA, which is only tens of seconds long, may not be ideally suited for establishing the time-average trends in reflooding situations. One should also consider the necessity, however, of accounting for flow oscillations noted in Section 3.2 since these do affect precooling and carry-over.

The period of the oscillations is 2–3 s. To numerically follow these, the codes have to use time steps of the order of 50 ms, although implicit or stability-enhanced semi-implicit integration schemes allow time steps of the order of the second, considering stability as the controlling limit. Small steps are dictated, however, by accuracy criteria. These small steps likewise mitigate the notorious (but unphysical) pressure spikes due to the sudden calculated release of heat from the fuel elements. The truly dynamic modeling of the details of heat transfer and fluid flow during high-frequency oscillatory reflooding can be considered as still being at the
edge or beyond the state of the art; this would have required transient forms of the closure relationships which simply do not exist (see Section 2.4). In spite of such considerations, the desire to use a single code covering all accidental situations has imposed use of the general purpose codes for reflooding analysis too. Simple but highly specialized reflooding codes (e.g. Yadigaroglu and Arrieta, 1981; Ghiasian et al., 1988) can reproduce at least the forced-feed experimental reflooding transients and the general trends of oscillatory reflooding probably much better, but these are not our subject matter here.

4.1 The Importance of the QF

Although during an accident scenario several quench fronts may appear in the core, during the classical, forced-reflooding rate experiments, the QF subdivides the channel in two zones: a downstream zone where the wall is hot and post-burnout heat transfer takes place, and an upstream "rewetted" zone, where heat transfer has returned to nucleate boiling.

Axial conduction is controlling the propagation of quench fronts for cases with negligible precursory cooling. In such cases the cladding temperature displays an abrupt cliff or almost a discontinuity. These are typically, the low reflooding rate, high wall temperature situations where heat transfer in the dry part of the bundle is poor. In a frame of reference moving with the QF, the temperature distribution in such cases looks like a standing wave. High-precursory rate cases (possibly with IAP6 at the QF) present a "collapsing wave" like temperature distribution and QF velocities an order of magnitude higher. Rewetting will be discussed below, but a few words about the importance of the QF and its impact on the choice of analytical descriptions of heat transfer are in order.

In most if not all existing system codes the heat transfer coefficient (h.t.c.) selection logic is based on the local-conditions hypothesis discussed in the next section, i.e. on the values of the local flow parameters and wall temperature. Thus rewetting occurs either when the wall temperature falls below a certain value or when an essentially equivalent rewetting criterion is met. Rewetting results in either an abrupt or a more gradual switch in the heat transfer mode, depending upon wall temperature and flow conditions. Using a wall rewetting criterion, if the cooling of the wall in the dry region is locally overpredicted even for a short period of time, rewetting may occur in several nodes simultaneously. Such multiple rewetttings are physically speaking not excluded, but they normally do not take place when rewetting occurs slowly through the progression of the QF.

The mode of heat transfer depends in reality on the relative position with respect to the QF, rather than upon the wall temperature alone. For example, a situation can occur where film-boiling like heat transfer takes place downstream from the QF, in spite of the fact that the wall temperature there is near saturation; this is simply due to the hydraulic conditions controlling heat transfer rather than the wall temperature alone. Such position dependent heat transfer mode is not unique to reflooding; numerous other examples of such "history" dependence exist (see e.g. Shiralkar et al. 1980).

Making the h.t.c. selection logic dependent on relative position with respect to the QF avoids having to deal with the nasty problems of premature or rapid quenching of nodes downstream and the resulting pressure spikes due to the sudden release of heat from the rods. Moreover, such
modeling can also take care of the heat transfer enhancement phenomena observed immediately downstream from the QF.

Thus, several authors believe that it is much better to apply post-dryout closure laws based on location with respect to QF, and not on wall temperature. This is particularly true in transition boiling where steep axial gradients occur. A position dependent closure law ensures proper "dumping" of the heat from the fuel rod into the coolant and produces a uniform progression of the QF. The use of correlations depending on the distance from the QF is discussed in Section 5.2.

There are, however, cases where several QF's start simultaneously (for example at spacer grid locations). This clearly complicates the logic that must be built into the codes, but it is likely to be worth the effort.

When the progression of the QF is axial-conduction controlled, its velocity must be predicted using an axial-conduction-controlled QF velocity model. There are difficulties in integrating a solution for the two-dimensional axial conduction problem in the quench region into the numerical structure of the codes. Usually, either a tight mesh moving with the QF or an adaptive-grid technique (inserting and deleting nodes as needed) are used for the quench region, but the noding must be extremely tight (in the millimeter range, O'Mahony, 1988) to assure correct convergence of the axial conduction calculations; this is usually not practical. This point is further discussed below.

4.2 The Heat Transfer Surface and the Local-Conditions Hypothesis

With the term "Heat Transfer Surface" (HTS) (or "Boiling Surface") (Nelson, 1975, 1982), the code developer usually associates the idea of a continuous multidimensional function without gaps or jumps describing heat transfer from the wall to the fluid. This function is typically dependent upon the local flow and wall conditions. Thus the notion of heat transfer surface has been traditionally associated with acceptance of a local-conditions hypothesis (i.e. dependence of heat transfer only on local conditions, excluding flow development, transport of turbulence, and other similar "history" effects). By extending, however, the notion of HTS and including other effects such as flow history (e.g. dependence from QF location in the case of reflooding), one can get away from this limitation (TRAC, 1988, pp 4-39 to 4-43).

To produce a good HTS, one prefers correlations that cover a wide range and leave only small areas where interpolation is needed. Certain workers may consider as a drawback that highly empirical but precise correlations with a narrow range of validity should be avoided. This produces a general best-estimate HTS less accurate for certain applications than alternative, specifically designed narrow correlations.

The HTS approach is no different than the programming of any set of closure laws for a code. The HTS was meant, however, to be a systematic procedure forcing the code developer to address issues that might be otherwise overlooked: First, a closure quantity must be provided for any potential point in the solution space by making a conscious decision instead of defaulting to an answer through an unclear understanding of the selection logic. Second, if in examining the solution space, a discontinuity is found, then the code developer should clarify whether it is physical or due to the selection logic. An application of the HTS concept is transition
boiling discussed in Section 5.3.

As we have seen, the question whether there are inherent limitations to the heat transfer surface concept can be answered if a clear definition of the term HTS is given. If the conception is a sufficiently general one and not limited by the local-conditions hypothesis, there should be no limits in principle. More practical questions in this respect seem to be:

- Is there really a unique heat transfer surface? and if so,
- do all heat transfer states on this surface depend solely on the local macroscopic fluid conditions or are there also history effects?

To the first question there is an easy answer: If really all parameters are included, there should be a unique functional relationship that can be represented by a multidimensional surface. It is clear that not only fluid conditions and wall temperatures have an influence, but also wall material properties, geometrical details, etc. Thus, different HTS's might be required for different processes or parts of the system, e.g. a HTS for reflooding of the core and one for a steam generator.

To the second question: there is enough evidence around to prove that history effects influence heat transfer. This is certainly true for the region immediately ahead of the QT. Several authors, including Rassokhin and Kabanov (1987), Shiralkar et al. (1980) report that dryout heat transfer does not only depend on local and immediately upstream conditions but maybe on the conditions over the entire test section. The case of particular interest here, enhanced heat transfer immediately downstream from the QT, is discussed below.

The problems outlined above do not principally question the HTS concept, but are meant as a warning against its careless application and its possible limitations when it is interpreted in a narrow sense.

5 HEAT TRANSFER DURING REFLOODING

The usual reflooding situations are characterized by low pressures and low mass fluxes. Under such conditions, the presence of two different main flow and heat transfer regimes has been widely recognized from the first reflooding experiments: these are the Inverted Annular Film Boiling (IAFB) and the Dispersed Flow Film Boiling (DFFB) regimes.

In a somewhat idealized IAFB situation, one observes a liquid core separated from the wall by a thin vapor film. The liquid may be subcooled, while the average flow quality is necessarily negative or only slightly positive. Indeed, under low pressure, already at very low quality the void fraction becomes rapidly very large and the liquid core is broken up, first into chunks of liquid, and then into drops. Thus an Inverted-Slug Film Boiling (ISFB) regime may succeed to the IAFB regime.
Although there is general agreement that heat transfer in the inverted-slug regime is important and should be modeled carefully, not much information explicitly related to this regime can be found; this is the reason why it is not treated separately in the following sections. It is often treated by interpolating the heat fluxes obtained from correlations applicable to the adjoining regimes. *

In DFFB the void fraction is high and the liquid is in the form of dispersed drops. DFFB may result from the breakup of an IAPB liquid core, or from further disintegration of the slugs in ISFB, or may appear immediately downstream of the dryout point in annular flow.

The "pure" forms of IAPB and DFFB may not be present over large segments of rod bundles during reflooding. Indeed, recent Cylindrical and Slab Core Test Facility, CCTF (Murao et al., 1983) and SCTF (Adachi et al., 1983; Iwamura et al., 1985), differential pressure measurements (average values over 2 feet) indicate void fractions mostly in an intermediate range. The flow regime downstream of the QF is characterized by a transition or froth region or a slug-type flow regime.

According to Kelly, differential pressure measurements performed during the PERICLES 368 heated rod tests at high flow rate (where IAPB could have been expected) indicate void fractions in the range 0.55 to 0.75 that would rather signal the presence of ISFB. The heat transfer measurements were, however, still in good agreement with IAPB model predictions (assuming void fractions in the range 0.1 to 0.3).

In the remainder of this Section, we will concentrate on post-burnout heat transfer. Indeed, there seem to be no special difficulties in modeling heat transfer in the wetted part of the channel, with the possible exception of certain discrepancies observed when tube correlations are applied to bundles (Hassan and Blanchat, 1989).

5.1 Modeling of Post-Burnout Heat Transfer with Two-Fluid Codes

The post-burnout heat transfer regimes relevant to reflooding are characterized by departures from thermal equilibrium and by large differences between the phase velocities. Heat transfer and hydrodynamics are closely coupled and influence each other. Without considering for the moment any radiative transfers, as well as exchanges due to direct liquid-wall contacts, heat transfer takes place mainly in two steps: the heat flux from the wall superheats the vapor in contact with it; the vapor then transfers some of its heat to the liquid. Although there is largely no direct liquid contact with the wall, it is still the presence of liquid in the channel that is decisive for heat transfer in these regimes. Thus predicting correctly the location and the quantity of this liquid is crucial.

If the liquid is saturated, the entire heat flux from the vapor to the liquid is used for vapor generation. If the liquid is subcooled, part of the heat transferred at the interface is conducted into the liquid and reduces its subcooling, while the remaining part produces vapor. This

* For an example of treatment of the ISFB regime (or "agitated inverted annular" regime) see Nelson and Unal (1992)
situation is described by the energy jump condition that can be written in simplified form as
\[ q_{i}^{v} = q_{i}^{L} + \frac{A}{P_i} \Gamma h_{L} \]
where \( q_{i}^{v} \) and \( q_{i}^{L} \) are the heat fluxes from the vapor to the interface and from the interface to the liquid, respectively; \( A \) is the cross-sectional area, \( P_i \) the interfacial perimeter, \( \Gamma \) the volumetric rate of vapor generation, and \( h_{L} \) the latent heat. One must specify closure laws for the two heat fluxes in order to calculate the vapor generation rate from the equation above. These laws depend from variables such as the relative velocity between the phases; thus the appropriate closure laws governing momentum exchanges between the phases and the phases and the wall must also be considered.

The two-fluid codes are especially well suited for modeling these situations, since they have the inherent capability of modeling the exchanges “wall-vapor-liquid” and the coupling between hydrodynamics and heat transfer. As presently used, however, the codes do not make full use of their capabilities. The main reasons for this are:

a) Several interfacial exchange laws are needed to close the system of equations. The interfacial exchanges are still poorly understood or depend from a variety of parameters that cannot be estimated with great confidence. Moreover, most of the time, the interfacial exchanges cannot be directly measured and the corresponding closure laws cannot be verified by direct comparisons with data. Thus, the models end up containing too many adjustable parameters and assumptions influencing the results. For certain codes, the droplet diameter is the most important or likely “adjustable” parameter.

b) There are usually difficulties in measuring under realistic conditions any parameters in addition to the wall temperature and the heat flux from the wall. Thus the intermediate steps in the calculations and the assumptions made for these remain largely unverifiable. A few relatively reliable vapor superheat measurements obtained with a probe extracting steam from a small region of the channel have been performed, however (Nishihara et al. 1980; Evans et al. 1985; Gottula et al. 1985; Ural et al. 1986; Tzula et al. 1987). Other parameters such as droplet diameter spectra and velocities have been measured, not always, however, under the same conditions as in a reactor core; moreover, there is no obvious way of introducing proper average values for these into the equations which remain one-dimensional while the problems can be 2- or 3-dimensional (Andreani and Yadioglu, 1992; Tzula et al. 1987).

c) In the absence of adequate interfacial exchange laws, the codes may still use older correlations describing heat transfer from the wall to the mixture; this is most often true for IAFB. The wall heat fluxes obtained from mixture correlations must then be partitioned between the liquid and the gas, or assigned to one phase. This is clearly an unfortunate situation and defeats the purpose of the two-fluid codes.

d) There are numerical and code-logic difficulties in implementing better physical models into the codes. The usual node sizes used for safety analysis are usually too large for capturing the necessary degree of spatial detail. For example, IAFB may take place over lengths of 0.1 to
0.3 m; this is of the same order of magnitude as the size of nodes typically used for LOCA analysis. Thus improved post-burnout heat transfer models can only be implemented for benchmark type calculations, unless one finds practical ways of making system calculations with smaller core meshes.

e) Other practical difficulties arise from the fact that investigators proposing better models for a particular heat transfer regime are often unaware of how other parts or features of the codes, in which their work might be ultimately used, affect their model. On the other hand, code developers may be tempted to use "models" not based on any physical reality, simply because they have a good effect on the stability of the code. Models for a particular heat transfer and two-phase flow regime should cover both the hydrodynamic and heat transfer aspects of the situation; both aspects should be implemented consistently in the code.

f) One should also note that the numerical stability of the solutions is often affected by changes made in the correlations and the logic of the codes, making code modifications difficult.

In spite of such difficulties, partial successes in implementing physically realistic post-dryout models in two-fluid codes exist; an example regarding IAFB is given in Section 5.4 below.

5.2 Enhancement of Heat Transfer Immediately Above the QF

Reflooding experiments clearly show a rapid (exponential-like) increase of the h.t.c. as one approaches the QF from the dry region. Unexpectedly high (enhanced) heat transfer takes place in a length extending some 0.2 to 0.3 or even 0.5 m above the QF (see e.g. Juvel, 1984; Seb et al., 1978; etc.). Enhancement here is the amount of observed heat transfer in excess of that predicted in that length range by a relevant film boiling heat transfer model based on the local fluid and wall conditions (e.g. wall and vapor temperatures, void fraction, mass fluxes, drop size, etc.). Enhancement of heat transfer near the QF appears under several heat transfer regimes and is discussed in this section, before moving to the discussion of the particular heat transfer regimes in the following Sections. Tracking of the location of the QF is an obvious necessity if one attempts to properly account for the heat transfer enhancements observed downstream.

The presence of enhanced heat transfer above the QF was realized early enough and Yu and Yadigaroglu (1979) correlated the space dependence of the h.t.c. h with empirical relations of the form

\[
h(z-z_{QF}) = h_{QF} \exp(-a\Delta z_{QF})
\]

(5.1)

where \(\Delta z_{QF} = z - z_{QF}\) is the distance from the quench front, and \(a\) and \(h_{QF}\) are coefficients, functions of local conditions at the QF (according to Yu and Yadigaroglu: quality, vapor generation rate at the QF, and the difference

* In IAFB, the heat capacity of the vapor film is low and the heat from the wall essentially goes to the liquid; thus assigning the wall heat flux obtained from a film boiling correlation directly to the liquid may be an inelegant and unnecessary, but acceptable expedient.
between the reflooding and QF velocities).

Such an approach was discarded as being too empirical, but it seems that it is getting accepted again. Indeed, it is necessary if one wants to include the important but complex heat transfer enhancement taking place immediately downstream from the QF.

Since enhancements such as those reported cannot be reproduced by the usual post-dryout correlations or models, an ad-hoc correlation of the type given by Eq. (5.1) above has been implemented in the CATHARE code (Juvel, 1984; Bosting, 1990). The formulation, which is valid only for low pressure reflooding and for void fractions above 0.93, i.e. essentially for ISFB and DFFB, is:

\[ q'' = q''_{\text{DFFB}} + q''_{\text{QFVI}} \]  \hspace{1cm} (5.2)

where \( q''_{\text{QFVI}} \) is the enhancement given simply as

\[ q''_{\text{QFVI}} = h(T_w - T_{\text{sat}}) \]  \hspace{1cm} (5.3)

with

\[ h = (1400 - 1880(z - z_{QF}))(1 - a) \]  \hspace{1cm} (SI units) \hspace{1cm} (5.4)

Figure 7 shows that the "excess" h.t.c. correlated reasonably well with the local void fraction \( a \) at various distances from the QF. The correlation line passes through the \( a=1 \) point for \( h=0 \). Figure 8 shows the correlation of the slopes of the observed linear dependency between \( h \) and \( a \) as a function of \( z_r/QF \).

Recently, CENG has examined the steady-state film boiling data of Winfrith (Sawage et al., 1989) obtained with the hot patch method, which contain void measurements in addition to wall temperatures. For conditions expected to be in IAFB or ISFB, at distances from the QF greater than about 0.15 m, no evidence of enhancement was found. The observed axial dependence of the heat transfer coefficient could be explained via the axial evolution of the void fraction (vapor film thickness). However, it is believed that the violent boiling at the QF does enhance the film boiling heat transfer immediately downstream and that this enhancement is important for the prediction of the QF propagation. Without such enhancement, a relatively high minimum film boiling temperature must be used to avoid the flattening-out of the temperature vs. time curve (and the delay in the quench time) associated with the use of a Bromley type correlation for film boiling.

An attempt to incorporate enhancement above the QF in a DFFB boiling model was made by Webb and Chen (1984) who proposed a two-region vapor generation rate formulation. There is enhancement of heat transfer in the region near the QF due to liquid-wall contacts. The Webb and Chen model has been implemented in the new TRAC/MOD2 version of the code (Nelson and Umal, 1992).

Certain EM codes use the modified Bromley approach (Andersen, 1976) for IAFB downstream from the QF. The modified Bromley correlation uses the wavelength for the interfacial instability in film boiling (= 1.5 cm for water) as a characteristic length and produces a constant rather then decaying h.t.c.. This procedure results in a conservative value of the h.t.c. within, say, 1.5 cm from the QF but gives a slightly higher value downstream. In a system calculation that is overall conservative, the
effect of a reduced heat release from the QF on core thermal-hydraulics has, however, also to be considered.

JAERI correlations in which the h.t.o. varies inversely proportionally with distance from the QF can describe the observed enhancement, a kind of precursory cooling. The JAERI heat transfer model for inverted-slug flow uses the \(1/z^{0.25}\) dependence from distance of the original Bramley film boiling result; it has been assessed against a large data base and produces significant "enhancement" up to 0.2 or 0.3 m above the QF.

Enhancements effects are clearly also present in transition boiling discussed in Section 5.3.

5.3 Transition Boiling

Transition boiling (TB) is the less understood of all boiling regimes. For reflood conditions where precursory cooling is significant, it is the regime responsible for the final quench. TB most likely controls the propagation of the QF, even though it typically exists only in the narrow QF region.

5.3.1 Physical Phenomena

In a fundamental study, Passeyopanakul and Westwater (1978) showed the influence of a material's ability to supply energy to the fluid during transition boiling and the quenching process; this is determined by both its transport properties and its geometry, in particular its thickness. Fluid conditions also affect transition boiling of course, with the void fraction being particularly important. The transition boiling regime may be well established at low void fraction but yet disappear at higher void fraction due to the lack of liquid available to contact the wall.

The view of transition boiling, as a regime in which a fraction only of the heated surface is contacted by liquid, has led to the classical formulation*

* Enhancement in a bundle during reflood is expected to be larger than that present in the Winfrith tests. More generally, one should note that there is a difference between "hot patch" steady state experiments in a tube and actual reflood with a moving front in a tube or a rod bundle. Although the liquid flow still "trips" over the dryout point, the heat flux spike present in actual reflood at the QF is reduced. Recent studies (Gottulla et al., 1985; Evans et al., 1985; Hood, 1987) have shown that the the QF is arrested *upstream* of the hot patch by heat supplied by axial conduction from the hot patch. The hot patch prevents the liquid that was just expelled from the wall from recontacting and quenching it. The nucleate boiling and TB regions in this case are very short. Comparison of results obtained using the hot-patch technique to those obtained by slow bottom reflooding over a very limited range of high-void, low-flow conditions (Gottulla et al., 1985) have shown similar heat transfer behavior; differences may, however, exist at lower voids and higher flow rates. Potential distortions introduced by the hot-patch include the shortening of the transition boiling region (down to 1 mm, Umal and Nelson, 1992) and modification of the IAPR regime inside the hot patch due to the prevailing higher temperatures; these may prevent wall contacts.
Fig. 7 Development of the CATHARE correlation for heat transfer downstream from the QF: variation of the h.t.c. with a at various distances from the QF.

Fig. 8 Variation of the coefficient \( h \) in the CATHARE correlation with distance from the QF.
\[ q_{\text{TB, total}} = f q_{\text{vapor}} + (1-f) q_{\text{liquid}} \]

where \( q_{\text{TB, total}} \) is the total wall heat flux in TB, \( q_{\text{vapor}} \) is the wall heat flux to the vapor phase, \( q_{\text{liquid}} \) is the liquid-phase wall heat flux, and \( f \) is the fraction of heated surface contacted by liquid.

The difficulties in understanding the fundamental processes involved in TB have led to several classical simplified approaches. The two earliest involve: 1) use of the above equation, and 2) pinning of transition boiling heat flux at the CHF and minimum film boiling (MFB) points and connecting the two with a simple function, typically a straight line in a log-log \((q, \Delta T)\) plot.

The first approach is hindered by lack of information about any of the three “independent” heat fluxes involved. The second method has been used widely. Although new experimental data on TB have recently become available, (e.g. Auracher and Albrect, 1984; Johannsen, 1988; Johannsen and Mosad, 1989; Weber, 1990), their applicability to quenching problems should be demonstrated; interpolation between the MFB and CHF points is still in use. Information about the CHF point is widely available; analytical description of the MFB point is also required, however, and this is much more difficult.

A third method occasionally applied, essentially a variation of the second one, is to define a priori some functional dependence for TB heat transfer from wall temperature, usually a decaying function from the CHF point.

The three methods mentioned above were originally conceived for pool boiling but they are indiscriminately applied also to flow boiling.

Several major problems exist when using reflooding models developed according to the second method. The main one is that the quenching process may be either thermodynamically or hydrodynamically controlled, depending on the flow transient involved (Nelson, 1982). Thus, the MFB point is not a unique point, such as the homogeneous nucleation point. Instead, MFB is potentially dependent on the flow transient itself.

Pearson (1984) described rewetting as the end point of the path taken as cooling progresses over a heat transfer surface (HTS). The shape of the HTS changes with distance from the quench front. In this topography, transition boiling is an uphill path between a valley (MFB) and a ridge (CHF) that becomes less steep as one climbs up. If the wall temperature at the CHF point, \( T_{\text{CHF}} \), is used as an anchor point for transition boiling or alternatively, if the transition and film boiling curves are used to determine the rewetting temperature \( T_{\text{MFB}} \), this really means that one interpolates between points that shift before they are reached. Thus one is trying to reach a moving target...

Regarding the determination of the minimum heat flux and \( T_{\text{MFB}} \), some new experimental information and interpretation can be found in recent publications by Auracher (1985), Weber (1990), and Shroeder-Richter (1991). Straightforward method for modeling the transition boiling region seems to be the “history effect” method introducing an empirical dependence upon distance from the QF. This could be considered as a fourth alternative, but it may require a distinct representation of the transition boiling region
for each flow geometry and structural material combination.

5.3.2 Code Models

Modeling of transition boiling in the two-fluid codes is based on the second and following methods noted above. Generally, the wall heat transfer law defines the total heat flux in the transition region. Then the contribution to the vapor film is evaluated at the prevailing flow and wall conditions and subtracted from the total heat flux to determine the heat flux to the liquid. Exceptions to this general technique will be noted.

TRAC-PF1/MOD1 uses the second method, i.e. the MFH temperature approach. As discussed by Nelson (TRAC, 1988, pp. 4-50 to 4-51), O'Mahoney (1989) observed a sensitivity to nodalization in the quench area due to this local conditions approach.

TRAC-PF1/MOD2 uses the fourth method, i.e. the history effect — dependence on distance to QF approach. Initial assessment of this model using primarily University of California-Berkeley (Seban et al., 1978) reflood experiments shows good prediction of the QF velocity.

In the CATHARE code, during reflood, the TB heat flux is calculated by the empirical Z2 model (Clement and Regnier, 1978): the surface heat flux is assumed to be proportional to the gradient of the surface temperature. The proportionality constant Z2 has been empirically correlated for a wide range of conditions as function of pressure, mass flux and quality immediately above the QF, and the "boiling length," i.e. the distance between the QF and point of net vapor generation. This model has been quite successful for prediction of QF propagation for bottom reflooding experiments at constant flow rate. Major problems exist for the specification (or the meaning) of the mass flux and quality at the QF during oscillatory reflooding or for alternate ECC injection (top to bottom quenching with liquid downflow from hot-leg injection).

The choice between models such as the ones implemented in TRAC-PF1/MOD2 and CATHARE depends largely upon the faith one has in their correlations. It is one of the major unresolved, although improved issues.

5.4 Inverted-Annular Film Boiling

Experimental observations show that the heat transfer rate in the IAFB region increases rapidly with liquid subcooling. Higher subcooling promotes heat transfer to the liquid core and reduces vapor generation and the thickness of the vapor film, thus enhancing heat transfer. At high flow rates, a strong increase of the heat transfer rate with mass flux is also observed. At low flow rates this effect may disappear.

Since IAFB superficially resembles film boiling in a pool, one is tempted into using pool boiling correlations to describe this regime. The classical Brumley-type analysis of film boiling does not account, however, for the effects of mass flux and subcooling mentioned above. Anderson (1976) proposed a "modified Brumley" correlation based on the assumption that the presence of waves on the interface causes the boundary layer to restart over each wavelength and eliminates the continuing decrease of the h.t.c. with distance, typical of growing laminar flow. This is in disagreement, however, with the experimental observations mentioned above showing a rapid decrease of the h.t.c. with distance from the QF.
The two-fluid formulation lends itself very nicely to implementation of IAFB models. Indeed, IAFB depends critically upon the interfacial heat and momentum transfer laws determining the rate of vapor generation and film thickness. Several two-fluid models of IAFB have been proposed (Analytis and Yadigaroglu 1987; Kawaji and Banerjee 1987; Denham 1983; de Cachard and Yadigaroglu, 1991; etc.). The interfacial exchanges are treated in similar way in these models. The vapor is considered as flowing between two parallel plates (the wall and the interface). The heat fluxes \( q''_w \) and \( q''_G \) from the wall to the vapor and from the vapor to the saturated interface, Fig. 9, then are typically given by classical expressions. Empirical corrections and enhancement factors may still be necessary to account for the fact that the flow of the vapor cannot be described as simply as above (Yadigaroglu and Andreani, 1989). Such enhancements may be due to turbulence in the films, violent vaporization at the QF, liquid contacts with the wall near the QF, the effects of the developing boundary layer in the vapor film, etc. They are beyond our present analytical capability.

There are also difficulties in estimating heat transfer from the interface to the liquid core, Fig. 9, since this involves a transient, turbulent-flow, entrance-length problem; discussion of these difficulties can be found in Yadigaroglu and Andreani (1989).

Certain experiments (Costigan and Wade, 1984; Imai and Dejarlais, 1987) have suggested images of IAFB that depart significantly from the rather simple picture given above. Nobody has attempted to model these situations; in fact since IAFB is not likely to be the dominating heat transfer mode under best-estimate situations, as discussed in Section 3, this may not be necessary.

![Diagram](image-url)

**Fig. 9** Interfacial heat and mass exchanges under IAFB conditions.

The various proposed IAFB models usually predicted correctly the experimental trends observed in rod or bundle experiments. According to Nelson, they have been implemented and tested in system codes with variable degree of success (see also Analytis, 1990). With rare exceptions, mechanistic IAFB models have not been permanently implemented, however, in
any of the large safety codes. The difficulties seem to stem from the implementation of the entire model within the existing framework of the code. This requires interventions in the heat transfer as well as in the hydrodynamics packages and can have an impact on numerical stability.

Numerical instability can arise when modeling the two-step heat transfer process at very low void fractions that are typical for IAFB. The low heat capacity of the vapor does not allow for much storage of heat, i.e. the vapor temperature rises quickly if the heat flow from the hot wall is not instantaneously passed on to the liquid. This practical problem may be one of the reasons why in some codes the main wall heat flux (not only the radiation part) is directly assigned to the liquid. This example shows that we have to live with practical solutions in the codes that are in apparent contradiction to the "true" process, as long as they are reasonable within the context of basic assumptions and simplifications in the codes.

Although it is rather evident that an ad-hoc IAFB model should have a better chance of correctly predicting IAFB situations, code developers keep in mind that their codes are to be used in a variety of situations. Thus very specific modeling of certain regimes is not necessarily considered as the optimal solution. IAFB is the typical example. As a consequence, in RELAP5 and the older versions of TRAC there are presently no explicit detailed representations of IAFB by appropriate and fully consistent sets of closure laws; the most recent version of TRAC (Nelson and Unal, 1992) is the exception.

For example, in RELAP5/MOD2, under typical IAFB conditions, i.e. when the reflooding heat transfer package is activated and the wall temperature is above a certain "rewetting" temperature, while the void fraction is lower than 0.91, heat transfer from the wall to the liquid is calculated using the maximum of the values returned by a transition boiling correlation and the modified Bremley correlation further "enhanced" by multiplying the h.t.o. by a factor

\[
\left[1 - 0.4(1 - s)^2\right]^2
\]

where \(s\) is the average void fraction. At the same time, the wall-to-vapor h.t.o. is correctly calculated using the Dougall-Bobenov (1963) IAFB correlation. According to the mechanistic modeling described above, there should not be direct heat transfer from the wall to the liquid, except that due to direct wall-liquid contacts. Moreover, the modified Bremley correlation is meant to apply to the local heat transfer from the wall (not to the liquid only). In spite of such real or apparent inconsistencies, the codes often predict IAFB behavior fairly well, especially for high flooding rates; the correlations apparently yield acceptably accurate h.t.o. values. Deficiencies are found at low flooding rates (Analytis et al., 1987). Some unification and streamlining of the logic and of the models and correlations built into the codes is certainly needed.

Figure 10 shows the cladding temperatures calculated by the "frozen version" of RELAP5/MOD2 for a reflooding test from the NEPTUN facility (Grueter et al., 1991). One sees with satisfaction that the experimental temperature trace is fairly well reproduced by the code (Analytis, 1989). A careful examination of Fig. 10 reveals, however, that the slopes of the temperature traces just before rewetting, i.e. in the region of IAFB, are quite different signaling a discrepancy in the heat transfer models used by
the code. This discrepancy is even more evident in the plot of the measured and calculated wall heat transfer coefficients, Fig. 11: while the experimental trace exhibits the typical sharp rise observed near the QF, the code predicts a slightly decreasing value of the h.t.c. This is due to the use of the "modified Breueley" correlation (Andersen, 1976) in RELAP5 under these conditions. One should also note that the values of additional parameters such as the vapor velocity and the void fraction calculated by the RELAP5 version including the mechanistic model were also much more "reasonable" than the ones calculated by the frozen version of the code (Analytica, 1989).

Ishii and DeMaria (1985, 1987) simulated the IAPF situation under both adiabatic and diabatic conditions with turbulent liquid jets enclosed in gas annuli. They measured velocities, jet length, shape, break-up mode, and the droplet sizes from the broken up core. Such information is valuable for the development of IAPF models.

5.5 Dispersed Flow Film Boiling

Unequal velocities and lack of thermal equilibrium between the phases (Andersson and Hall, 1981; Lee et al., 1982) are present and play again an important role with respect to heat transfer in DFFB: the droplets, starting from their entrainment position, are accelerated by the drag forces created by the higher-velocity steam flow. A terminal velocity is practically never attained. Correct modeling of the phase velocity difference is important since, first: all the interfacial transfer mechanisms are affected by the relative velocity between the phases, and second: predicting the distribution of the liquid correctly is crucial for post-dryout heat transfer, as already noted. The liquid is indeed the ultimate heat sink; the velocity of the droplets determines the concentration of the liquid phase along the channel. Chen (1986) and Andreani and Yadigaroglu (1989) summarized the state of the art in DFFB.

DFFB is governed by the two-step heat transfer mechanism introduced above. One can add to these two steps direct radiative heat transfer from the wall to the droplets, and possibly radiative heating of the vapor. Radiation between surfaces at different temperatures within a rod bundle may also have to be considered, especially in modeling small scale experimental bundles. Radiative heat transfers are very important at high wall temperatures, but could be already significant at relatively modest wall temperatures (already at, say, 600°C under certain conditions; Andreani, 1992). Thus radiative heat transfer may become important in reflooding cases which are part of a conservative system and core calculation (long time to reflood, large radial/axial peaking factors etc.). Wall to vapor radiation appears to be important only for high pressure/low flow conditions (small break).

On the contrary, at low wall temperatures, direct-contact heat transfer between the droplets and the wall seems to have an importance. Direct contact heat transfer takes place as "dry" or "wet" collisions.

In the case of dry contacts, the droplet approaches the wall, cannot touch it (either because it has not sufficient radial momentum, or because the wall temperature is above a rewetting temperature), vapor thrust due to asymmetric vaporization repels it, but part of it is evaporated (e.g. Kendall, 1978).
Fig. 10 Cladding temperatures from the NEPTUN E-5050 run and predictions using the frozen version of RELAP5/MOD2, as well as a special version including the Analytis and Yadigaroglu (1987) model.

Fig. 11 Heat transfer coefficient measured during the NEPTUN E-5050 run and predictions using the frozen version of RELAP5/MOD2, as well as a special version including the Analytis and Yadigaroglu (1987) model.
In the case of wet contacts (with a wall below a certain rewetting temperature) the droplet "wets" the wall and a large fraction of its volume is evaporated; this can be considered as similar to transition boiling.

There is also important enhancement of wall convective heat transfer to the vapor due to increased turbulence near the wall by the drop interfacial shear (Kianjah et al., 1984) and the presence in the boundary layer of evaporating drops acting as local heat sinks for the vapor (Chen et al., 1984). Kianjah et al. studied heat transfer enhancement due to the presence of small glass particles in a stream of air and report enhancements as large as a factor of two.

With a few exceptions (Iloeje et al. 1975), the effect of liquid contacts is considered most often in a purely empirical fashion. An investigation of several post-dryout models has revealed large differences especially in the wall-liquid component (Vojtek, 1990). The differences are even more pronounced at low pressure.

The main challenge in all the mechanistic DFFE two-fluid models lies in the determination of the superheat of the vapor. Indeed, once this superheat is known, the estimation of the heat flux to superheated steam is rather straightforward, with the exception of some secondary effects (Andreani and Yadigaroglu, 1989). The superheat of the vapor must be obtained from a heat balance involving heat transfer from the superheated vapor to the liquid droplets. Interfacial heat transfer controls the outcome and is the product of the interfacial area times the vapor superheat. Thus one must know the distribution of interfacial area (i.e. the spectrum of droplet sizes), as well as the relative velocity between the droplets and the vapor controlling the interfacial heat transfer mechanism. The interfacial area depends not only on the void fraction distribution, but also from the spectrum of droplet diameters. Interfacial closure relationships also depend on droplet diameter. Thus the droplet hydrodynamics already discussed in Section 3.5 become very important.

5.5.1 DFFE in the Safety Codes

Mechanistic two-fluid models of DFFE were already developed around 1967 in the UK (Bennet et al., 1967) and the US (Laverty and Roosnow, 1967; Foralund and Roosnow, 1968). Numerous models followed this early work, adding various degrees of complexity and sophistication. These, as well as the basic modeling approach, are discussed in detail by Andreani and Yadigaroglu (1989).

In calculating DFFE situations with the safety codes, the individual heat flux components (convection, radiation, etc.) from the wall to the vapor and the liquid are modeled separately. The obvious advantage of this procedure is that an arbitrary split of the total flux between the two phases is avoided. A difficulty we are faced with is, however, that the adequacy of modeling of the various terms cannot be verified independently. Interfacial and wall heat transfer influence each other. Modeling of DFFE is a very highly coupled problem of thermo- and fluid-dynamics. One cannot expect good heat transfer predictions with poor fluid-dynamics. Heat transfer modeling should not be manipulated to make up for deficiencies in fluid-dynamics.

The closure laws used in the safety analysis codes are often inconsistent in the case of DFFE also. For example, in TRAC-PF1/MOD1, for all post-burnout heat transfer, the wall-to-liquid heat transfer was essentially...
calculated adding to the radiation contribution the sum of h.t.c. from the modified Bramley and the Forslund-Rohsenow correlations; each of these correlations describes, however, alone heat transfer in a different flow regime.

The vapor-to-droplet heat transfer is usually modeled by a correlation for heat transfer to drops in superheated steam; this is typically the Lee and Ryley (1968) correlation which is, however, based on a very low range of vapor superheats. Its use in high vapor temperature environments is reckless because of the effect of the vapor properties (Andreani, 1992). Moreover, it predicts higher heat transfer rates than other correlations; the Rensszwalbult and Yuen (1983) correlation may be a safer alternative.

Modeling of the DFFB regime in TRAC-FF1/MOD2 relies on the Webb and Chen (1984) correlation for the wall-to-vapor h.t.c. This correlation considers the effects of heat transfer enhancement near the QF (discussed in Section 5.2) and the effect of entrained liquid droplets. A correction is, however, introduced by "tuning" a second term, which is the modified Bramley correlation corrected by a multiplier depending on the vapor Reynolds number (Nelson and Unal, 1992). Extensive assessment of the TRAC-FF1/MOD2 model with data from various sources has shown good agreement.

Radial distribution effects cannot be taken into consideration by models considering the flow channel in one dimension. The radial distribution of the droplets and the radial temperature profile are, however, important as recent detailed investigations show (Andreani and Yadigaroglu, 1992): the droplets act as local heat sinks, while the superheat of the vapor is the force driving the heat exchange; thus the rate of vaporization depends on the combined effects of the two distributions. Clearly, consideration of such effects, no matter how important they may be, is beyond the scope of the safety analysis codes. Indeed:

- The vapor temperature is supplied by the codes as a cross sectional average over a channel or a number of channels only; near wall effects are neglected.
- Vapor and liquid velocities are supplied as void fraction weighted cross-sectional averages only.

Some of the limitations due to neglect of the radial-distribution effects might eventually be overcome by employing methods that were successfully applied to other interfacial processes. For the problem of radial droplet concentration profiles, for example, the drift flux models could provide valuable information. For condensation in dispersed flow, where similar interfacial processes have to be described, modeling was improved by applying a local relative velocity instead of the difference between the averaged phase velocities (Hobbs, 1991). The problem of accounting for the combined effects of droplet and vapor temperature distributions, remains, however.

Most of the codes neglect the presence of the spacer grids, as already mentioned in Section 3.5. Modeling of such effects is not so important for conservative safety analyses and is incorporated only in the COBRA/TRAC code (Thurgood et al. 1983).

Modeling of radiation heat transfer between the wall and the mixture is not an easy task, as already mentioned. For void fractions below 0.35, its contribution is always large, especially under low heat and mass flux conditions. All codes use the network method for radiative exchanges which is valid for optical thicknesses well below the ones existing under such
conditions (Andreani, 1992).

Finally, DFFB models become inadequate near the dryout or quench point; they cannot account for the observed enhancement discussed in Section 5.2.

Figure 12 (Ralph et al. 1989) shows, as an example, the variation of the wall temperature and of the void fraction along a test section, downstream from a hot patch used to stabilize the quench front. The conditions were typical of reflooding experiments. Predictions obtained using the TRAC-PFI/MOD1 code are also shown. The code predicted the void fraction well, while it significantly underpredicted the wall temperature.

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**Fig. 12** Variation of the wall temperature (a) and of the void fraction (b) downstream from a quench front stabilized using the hot patch technique. Pressure = 4 bar, mass flux = 150 kg/m²·s, subcooling = 5°C. The solid line is a TRAC-PFI/MOD1 calculation for the power level of 3840 W (Ralph et al., 1989).

The limitations discussed above result in poor predictions of experiments where DFFB conditions prevailed. Examples of such calculations with TRAC-PFI are given e.g. by Afifi (1985), Chen (1987), and Akimoto (1987). Analogous difficulties in predicting the wall temperatures in reflooding experiments were also reported for RELAP5/MOD2 (Analytis et al., 1987).
Hassan, 1987). Even though in many cases the above researchers were able, by modifying the wall heat transfer and interfacial exchange packages, to match the experimental data within an acceptable level of accuracy, it is still doubtful whether much better general solutions of the problem can be reached with the actual structure of the codes. The main reasons are the simplified description of droplet hydrodynamics and the disregard of the influence of the dispersed phase on the wall-to-vapor heat transfer. In particular, neglect of the radial distribution and droplet spectrum effects, of the initial conditions, of droplet breakup and coalescence, of the effects of dry and wet droplet contacts with the wall, as well as of any effects of flow unsteadiness (bursts of droplets), may be inherently limiting the accuracy of the predictions. Nevertheless, the accuracy of post-burnout heat transfer calculations has already been improved by using the actual vapor temperature derived from the two-fluid models in the modern system codes compared to the use of old equilibrium mixture correlations in older codes.

One should also be aware of certain additional limitations we are still faced with when modeling DFFB with system codes: The water distribution in the liquid deficient region is the most influential parameter; uncertainty in this distribution comes not only from limited knowledge of the fundamental aspects such as the entrainment process, but also from system aspects such as core-wide water distribution and fallback from the upper plenum. Such effects were discussed in Section 3.2.

5.6 JAERI Phenomenological Approach

In the presence of IAPF with a very thin vapor film (experience gained from tube experiments), the superheating of the vapor does not take away a significant amount of heat from the wall, as already mentioned above, and one can assume that all of the heat from the wall is transferred directly into the liquid phase; no consideration of interfacial heat transfer is thus needed. This much simpler approach is adopted by JAERI which proposes to use in this case the Sudo (1980) correlation for the h.t.c. in subcooled film boiling.

For DFFB, a two-step model is applied, however; steam superheat and wall temperature are very sensitive to the choice of droplet size, density and relative velocity and some careful tuning is necessary. JAERI tuned the critical droplet Weber number using data from FLECHT low flooding rate experiments.

For Inverted Slug flow in bundles, JAERI proposes the Murao-Iguchi (1982) void fraction correlation, coupled to the Murao-Sugimoto (1981) correlation for the h.t.c. which again assumes that all of heat from the wall is transferred directly to the liquid phase through the gas phase.

The JAERI modeling of the various regimes described above, and use of a QF velocity correlation, has resulted in stable and fast calculations. The models were converted to a two-fluid formulation and built into the TRAC-PF1/MD1 code.
6 COUPLING OF HYDRODYNAMICS AND HEAT TRANSFER

From the very beginning of heat-estimate code development using the two-fluid formulation, it was intended to develop mechanistic process models. This means that the parameters that determine interfacial and wall transfer processes, such as interfacial area, relative velocity, etc., should be applied consistently for both momentum and heat transfer. Flow regime and heat transfer regime maps built into the codes should correspond just as these regimes do physically.

In practice, the situation in the codes does not yet reflect this ideal situation. Flow regime maps are usually dependent only on void fraction and mass flux, although it is well known that for some regimes the wall heat flux has also an influence. The selection logic for heat transfer regimes, in turn, is mainly based on wall superheat, using void fraction or vapor quality as secondary parameters. Thus there is not enough correspondence between the two yet. For example, for an IAER regime to exist, both subcooling of the liquid and high wall temperatures are needed; both considerations should have been built into the logic of selection leading to the IAER regime. This is not, however, the case; several other similar situations are not properly addressed by the codes.

There are recently encouraging attempts to harmonize the maps. Wall friction models, e.g., can easily distinguish between wet and dry walls and reduce wall friction to the liquid in film boiling. The recent modeling of reflooding in TRAC-FFI/MOD2 (Nelson and Umal, 1992), already discussed in Section 3.4.2, is an example of improved coupling between hydrodynamics and heat transfer.

Another family of coupling problems appears in relation to space discretization. In principle, in modern codes, an arbitrary number of heat slabs may be coupled to a single fluid cell. There are situations, however, where a mixture level delineates two entirely different heat transfer regimes, e.g., in the core during partial uncovering. Some codes explicitly track the rise and fall of such two-phase levels. For example, the ATHLET code treats the tracking of a mixture level by considering a vertical stack of control volumes. The two-phase level may fall or rise along the track. The volume actually containing the level is treated as a non-homogeneous cell. Since heat transfer above and below the mixture level is so much different, two separate heat slabs with a moving boundary would have been desirable.

It can also be said that the heat transfer and hydrodynamics coupling schemes should be "good natured," i.e., a kind of stabilizing feedback between the two should exist, to damp the numerics. JAEK has carefully checked this feature during the development of the REFLA and REFLA/TRAC codes to reduce the likelihood of instability in the calculations.

7 QUENCHING

The terms quenching, sputtering, minimum-film-boiling point (MFB), return to nucleate boiling, and Leidenfrost phenomenon are often used interchangeably to refer to various forms of wetting. These terms should, however, not be exactly synonymous, although certain authors believe that a
unique thermodynamic phenomenon having to do with the possibility of maintaining liquid contact with a hot wall is governing all these situations. It is evident, however, that the heat transfer and hydrodynamic mechanisms that play a role in all these cases can be quite different, leading to a large variation in the measured rewetting temperatures. Quenching phenomena in relation to reflooding have been briefly reviewed by Yadigaroglu (1988) and earlier by Elias and Yadigaroglu (1978).

Quenching of the cladding under typical reflooding conditions is considered in this section. It is generally agreed that axial conduction of heat inside the cladding from the hot dry region to the rewetted part plays a certain role in quenching. Many experiments have shown that quenching is a rather steady process. Even with oscillatory flows at the bottom of the core, the progression of the QF is not altered much (Kawaiji et al., 1985; Oh et al., 1986). The necessity of tracking the QF in order to correctly assign heat transfer regimes upstream and downstream has already been mentioned in Section 4.2.

All attempts to predict quenching as a spontaneous event that is triggered by the local cladding temperature falling below a predefined temperature, typically the apparent quench temperature or "knee temperature," had a limited success, as Fig. 13 clearly shows. It is generally not possible to predict quenching of a specific location on the basis of local conditions alone. Muro's correlation (also plotted in Fig. 13) provides a "Leidenfrost" temperature, while the apparent quench temperatures plotted in the figure are higher than this rewetting temperature and functions of QF velocity, physical properties, pressure and subcooling.

![Fig. 13 Comparison of experimental and calculated "MFB" temperatures using various correlations. (Hasan, 1989)](image)

Several authors have referred to the knee of the typical temperature curve obtained during all reflooding experiments as the rewetting point. However, if one considers the advance of the QF, and the simultaneous slow cooling
of the wall ahead of it by pre-quench heat transfer, it becomes evident that this temperature happens to be the instantaneous value of the wall temperature (as determined by its cooling history from the beginning of the reflooding) at the time at which that particular point is reached by the advancing QF. Indeed, there are experimental temperature traces, mainly for bottom reflooding at low flow rate or dispersed-flow cooling from the top, where no significant precooling took place and no change in the fluid quality or velocity was observed just prior to quenching. The only explanation for the sudden wall temperature drop in these cases is the arrival of the QF at that location.

This doesn’t necessarily mean that axial conduction alone is always the dominating mechanism. The approach of the QF may cause enhanced heat transfer to the fluid just as well. It is generally recognized today that both mechanisms, axial conduction and enhanced or precursory cooling, contribute, but the relative importance of each is still under dispute. This seems strange, at first sight, because axial conduction can be calculated exactly, although not without numerical difficulties. The temperature distribution near the QF is strongly influenced, however, by the heat flux profile just ahead. This interaction makes it difficult to separate the two contributions using available data. Nelson (1982) has investigated the complex interaction of fluid dynamics, thermodynamics, and conduction during the quenching process.

It is clear, however, that when the wall temperature is very high, the QF velocity is very low and QF propagation is controlled by heat transfer downstream (precursory cooling). In this case, codes where a very high rewetting temperature is specified, can predict quench times well but quench temperatures poorly; clearly precursory cooling should be carefully modeled in this case. When the wall temperatures are very low (possibly lower than the specified rewetting temperature), the existence of a sufficient amount of water in the channel is the necessary condition for rewetting; in this case the liquid fraction should be predicted carefully. In the remaining cases, axial conduction should play an important role. Quenching models incorporating similar logic are implemented in the JAERI codes.

With respect to the development of system codes three requirements may be derived from the facts stated above:

- Axial conduction should be included in the rod models. This requires a two-dimensional conduction calculation in a sufficiently fine mesh. Alternatively, if fine mesh is not used, axial conduction may be implicitly included using analytical expressions of axial conduction combined with ad hoc correlations to predict QF velocity.

- Quench fronts should be tracked in the codes. This can either be done by explicitly calculating the advancement of the front or by detecting such fronts from steep gradients in axial temperature using a sufficiently fine mesh. The need for this was discussed in Section 4.1.

- Heat transfer enhancement just ahead of the QF should be considered. Since the mechanisms involved are not yet completely understood, modeling will have to rely on empiricism for the time being.
7.1 The Axial Conduction Problem

The formulation of the axial-conduction-controlled rewetting problem is straightforward but its solution rather formidable. In the most general case of a fuel rod made up of fuel and cladding separated by a gap, one must solve the two-dimensional and time dependent conduction equation in the fuel and cladding regions, subject to arbitrary boundary conditions at the cladding surface, usually a h.t.c. function of the local cladding surface temperature, although the h.t.c. should also depend on the adjoining fluid conditions.

The classical solutions to this problem for a ribbon were reviewed by Elias and Yadioglu (1978). More recent solutions for a fuel rod with a single-step variation of the h.t.c. have also been presented (Ikeh, 1980; Mek, 1988). More complex functional dependences of the h.t.c. on wall temperature must be treated numerically or semi-analytically. What remains now to be done is to correlate the parameters describing the variation of the h.t.c. with local conditions to predict QF velocity for fuel rods also. In fact there is no assurance that the h.t.c. correlations (such as those of Yu et al., 1977) derived using ribbon data would remain valid for fuel rods. The reason is that although heat transfer from the surface of the cladding should in principle not depend on anything but the surface temperature and the flow conditions, the idealization of the h.t.c. variation may be tying the particular correlations obtained to the particular axial conduction solution used.

Under "normal" reflooding conditions, axial conduction can be neglected only if there are no sharp discontinuities in the heat transfer coefficient distribution that produce, in turn, steep axial temperature gradients. In numerical conduction calculations such steep axial temperature gradients can be reproduced only if the nodalization is very tight; unfortunately one finds in the literature statements to the non-importance of axial conduction based on results obtained calculated with with relatively coarse meshing with and without axial conduction.

Many analytical solutions, as well as all numerical ones have convergence difficulties at high QF velocities and for abrupt variations of the h.t.c. This is an inherent difficulty of the problem. Numerical solutions require axial meshing as small as 0.1 mm (O'Mahoffy, 1988) for proper convergence. Incorporation of such calculations into the main stream of computations of safety codes is impractical. Clever methods for performing these calculations separately must be implemented.

7.2 Tracking of the Quench Front

There are two basic approaches for tracking the QF: a) use of a two-dimensional (radial-axial) conduction calculation for the fuel together with a "boiling curve," specifying the variation of the heat flux in the vicinity of the QF; in this case an accurate description of the boiling curve is required (this is not easy, since the accuracy of measurements during quenching is poor), and b) use of a QF velocity correlation combining an analytic solution of the axial conduction problem with some empirical information about typically a rewetting temperature and one or more idealized heat transfer coefficients describing heat transfer near the QF. In principle, the methods are equivalent, although in practice not, because of the idealizations involved.
Considering now the situation in the safety analysis codes, we see that TRAC used the first approach, but without necessarily treating the axial conduction problem correctly because of the relatively large mesh sizes imposed. A correlation yielding rewetting temperatures in the range 700 to 900°C is built into the code to determine the point at which heat transfer switches to transition boiling. This could have been the "knee" point in case of reflooding.

In the RELAP5 logic there is no explicit consideration of a rewetting temperature. The intersection of the modified Bromley with the transition boiling correlation plays this role, provided it is higher than a thermodynamic "rewetting" temperature. The heat transfer mode switches to transition boiling below this intersection point. Thus both codes, as well as most other US codes, do not model rewetting according to the progression of a QF, although the necessity of following the progression of the QF and of tracking its location has already been mentioned above.

In other codes such as CATHARE (Barre and Bernard, 1990), RELFA, RELFA/TRAC, and FLUT (Hora et al., 1986), the second approach mentioned above is followed and the position of the QF is obtained by calculating a QF velocity and updating the QF location accordingly.

The use of an analytical quench velocity correlation is not incompatible with the assumption of a continuous boiling surface. Such correlations are a means of considering axial conduction, while avoiding the complication of a sufficiently fine mesh, as already stated. If properly included in a general heat transfer surface logic, analytical quench velocity correlations can be an adequate technique for system codes. Indeed, such an approach was successfully applied for calculating both upper and lower QF positions in large bundle experiments (Hora and Teschendorf, 1986).

Acknowledgement

The first author (GY) is highly indebted to H.-M. Fries for his efforts regarding preparation of the manuscript. He also wishes to acknowledge valuable comments from G. Th. Analytis, M. Andreani and I. Yadigaroglu.

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ABSTRACT

During a Loss of Coolant Accident (LOCA) in a Pressurized Water Reactor (PWR) important condensation phenomena take place in the neighborhood of the Emergency Core Cooling (ECC) injection system. Direct contact condensation has been a central issue for thermalhydraulics codes such as CATHARE (Code for Analysis of Thermal Hydraulics during an Accident and for Reactor safety Evaluation). The COSI experimental program was developed to simulate and study ECC injection.

The ECC injection has a strong local effect which requires a specific modelling. The condensation rate is controlled by the turbulent heat transfer in the liquid. A model directed toward a physical based approach is developed which incorporates the effects of such factors as jet diameter, conduit size and flow distribution. The predicted and experimental condensation rates are compared for different test conditions.

An experimental test series was devoted to study the effects of non-condensable gases on the condensation rate. An analysis of the results and a comparison with the predicted values is presented.

The objective of the present study is to describe the physical mechanisms involved at the ECC injection, but also to point out some experimental and numerical limitations when modelling complex physical phenomena such as direct contact condensation.
INTRODUCTION

During a Loss of Coolant Accident (LOCA) in a Pressurized Water Reactor (PWR) direct contact condensation occurs due to the Emergency Core Cooling (ECC) injection. This phenomenon may have a significant effect on the transient particularly when it gives rise to unstable flows (Block, Kirchner and Bankoff). The condensation depends on complex physical mechanisms such as two-phase flow dynamics, turbulence characteristics and non-condensible diffusion at an interface. System codes such as RELAP 5, TRAC, CATHARE, and ATHLET should be able to describe these effects which are of relevance for safety analysis. This paper reports some difficulties encountered in modelling condensation. A particular point is developed concerning the local effect of the ECC injection on the condensation.

Many experimental and theoretical studies have been directed towards modelling pressure and flow transients induced by the ECC injection interaction with the flowing steam (Block, Rothe et al., Aya et al., Kirchner and Bankoff). Several studies on direct contact condensation have appeared in the literature showing that a difficult task in modelling such phenomena is the understanding and description of the controlling physical mechanisms (Bankoff et al., Lee et al., Hughes and Duffey). The simple case of a co-current and counter-current flow of steam and subcooled liquid is first examined showing the importance of the liquid turbulence field. Then the conditions for the occurrence of flow instabilities are reviewed as well as the related modelling aspects. Local effects can determine the global stability of the flow. The COSI test facility simulating the ECC injection system is presented to investigate the modelling of local effects at the injection area. Various models are compared with the experimental results suggesting that the condensation is governed by the specific local turbulent field in the liquid phase which depends on various parameters such as pipe diameter, injection line diameter, flow distribution and injection flowrate.

Some experimental and numerical limitations when developing a physically based model for a reactor safety thermalhydraulic code are discussed.

REVIEW OF CONDENSATION PROBLEMS AT ECC INJECTION

Direct contact condensation at ECC injection occurs in all LOCA transients. The associated flow pattern mainly depends on the size of the break. In relatively small break size the injected liquid mass flowrates are small and stable flow patterns like stratified flows occur in the injection legs. In large break LOCAs very large injection flowrates may occur particularly at accumulator discharge. These situations are characterized by a strong thermo-mechanical coupling. Strong instabilities may be created by high instantaneous condensation rates. Interfaces are rapidly suppressed and a phase separation is installed. The reduced interfacial area will be sensitive to all mechanical perturbations which will induce a high condensation rate. This high mass transfer may itself create large scale liquid movements. Oscillations of variable amplitude and frequency may take place. This is penalizing for both experimental and theoretical investigations. From the modelling point of view, there is no more a clear separation between time and space scales characteristics of the mean flow and the scales responsible for the heat and mass transfers.

P.H. Rothe and B. Wallis presented an analysis on pressure and flow oscillations in a PWR cold leg at the ECC injection system and compared their predictions with experimental results from the Creare test facility. They summarized their results with a flow regime map where oscillatory and
no oscillatory conditions were reported depending on a single parameter, the thermodynamic ratio, defined as,

\[ \frac{T_{S} - T_{T}}{T_{S}} = \frac{W_{I} C_{L}}{W_{S} h_{LG}} \]

where \( W_{I} \) is the injection mass rate and \( W_{S} \) the steam supply rate. This parameter expresses the capability of the subcooled water to absorb the latent heat from the steam. They reported that a thermodynamic ratio greater than unity is a necessary condition for the maintenance of liquid slugs in the cold leg. According to this study the possibility of water plugs in the cold leg can not be ruled out.

J.A. Block reviewed some condensation driven fluid motions such as countercurrent flows, annulus flow oscillations, liquid bypass at the break and flooding at the upper plenum. Moreover this paper examined some rapid condensation transients in horizontal flow such as jet condensers, cold leg flow oscillations and condensation induced waterhammer. A "universal" map for direct contact condensation is proposed which characterizes the interface movements. Interface excursion, fluctuations, oscillations or stationary interface are the four possibilities. The line \( R_{T} = 1 \) separates the map into two major regions where complete condensation of the steam is not possible \( (R_{T} < 1) \) with the interface excursion regime and where it is possible \( (R_{T} > 1) \). The separation between the three other regimes depends on the liquid subcooling, the vapour flowrate \( W_{S} \) and the liquid flowrate \( W_{I} \). Unfortunately this map is dimensional and geometrical parameters are likely to have an influence on the occurrence of the different regimes. Block also reports that other parameters should be taken into account concerning the response of the whole system to the local condensation. He considers the case of the "hard steam supply" with a high hydraulic resistance for the vapour in its way to the condensation area. This case favours large amplitude oscillations. In the "softer steam supply" with a low resistance to the steam flow, large pressure oscillations are not possible and higher frequency fluctuations may occur.

These studies about instable condensing flows emphasizes the difficulties to predict such situations. Yet they are relevant for safety as plug formation in cold legs and intermittent water delivery to the downcomer is observed even in scale 1 UPTF tests (Weiss). The importance of system effects on plug movements makes it impossible to extrapolate an average behaviour from experiment to reactor transient. Fully reliable safety codes should predict plug movements. This would require a modelling of the axial thermal transfers near the plug interface and a very fine meshing. Unfortunately present system codes cannot prove high capabilities in this field. The most satisfactory results are obtained when codes can predict well an average condensation rate and a correct refill process in simulated large break LOCA tests (LOFT, UPTF...).

While most of the studies on these complex transient phenomena appeared in the applied engineering field such as reactor safety analysis, fundamental research focused on 'steady state' condensation in co-current and counter-current stratified flows. Various models describing heat and mass transfers at an interface have been proposed either in terms of surface renewal theory (Banerjee, Hughes and Duffey, Sonin et al., Hobbhahn) or Reynolds averaged turbulent diffusion (Bankoff and Kim, Lee et al., Theofanous, Thomas, Segev et al.).
The Reynolds averaged turbulent diffusion approach was proposed by Theofanous et al. for mass transfer correlation and based on a synthesis of the large-eddy and the small-eddy models. The analogy for heat transfer correlations was presented by Bankoff and Kim who predicted local condensation rates in countercurrent stratified flows with a turbulence centered model.

The renewal surface theory has been widely used in chemical engineering applications to describe heat and mass transfer. It approximates the mechanism taking place at the gas-liquid interface and assumes that parts of the liquid are renewed with fresh material from time to time. The time scale associated with this theory, \( \tau \), represents the average age of the various surface elements. Sonin et al. reviewed different models for the renewal time and the turbulent diffusivity. The dependence of the heat transfer coefficient on the turbulent Reynolds number varies from a power of 1/2 to a power of 3/2 depending on the assumptions made about the controlling physical mechanism.

In 1-dimensional 6-equations reactor safety code the heat and mass transfer are implemented through the liquid to interface heat flux, \( q_{li} \). In case of direct contact condensation the experimental data are used to obtain a correlation of the form,

\[
q_{li} = a \cdot h(P, a, R_1, V_1, V_r, T, T_{sat})
\]

where \( a \) is the exchange area to total volume ratio and \( h \) the heat transfer coefficient per surface area. This coefficient is assumed to depend only on local flow parameters.

When modeling condensation on stratified flow classical types of correlation are used in the form of a Nusselt number function of Reynolds and Prandtl numbers. Other possible effects are not described. The large temperature gradients observed in the liquid induce a strong density stratification which has an influence on the turbulent field. The corresponding effects on the heat transfers could be correlated as a function of a Richardson number. But no such effects were described probably by lack of experimental information. Moreover it is usually assumed that the condensation rate do not alter the turbulence structure in the liquid. This is certainly a very nice assumption to avoid instabilities in calculations but does not correspond to the actual physical behaviour. Studies concerning stratified flows (Segev et al., Kim and Bankoff) mention the existence of a highly agitated interface with three dimensional waves in case of high subcooling. This may be interpreted as the result of the non homogenous effects of the momentum of the condensing vapour which is transferred on colder parts of the interface. The double action of dynamics on heat transfers and of thermal non equilibrium on mechanical aspects must be noticed. A positive feedback is possible inducing catastrophic condensation peaks as observed by Sonin et al.

**COSI TESTS ANALYSIS**

The experimental modelling support is provided by the COSI test facility that simulates the ECC injection system as shown in Figure 1. The experiment is scaled 1/100 for volume and power from a 900MW FRANATOME PWR. Various flow configurations are available including concurrent and countercurrent stratified flows with adjustable liquid height. The model test series conditions have been chosen to represent thermohydraulic conditions during a small break LOCA and were conducted at pressure ranging from 2 to 7 MPa with different injection water temperatures and flowrates.
D. Bestion and L. Gros d'Aillon presented the experimental results and proposed a set of correlations.

![Diagram of COSI test facility.]

**Figure 1: COSI test facility**

A first analysis suggests three different flow regions A, B and C as shown in Figure 2. The qualitative results for each region and models for the condensation on the jet and in region C are presented in this section while a detail analysis of the condensation process in region B is presented in the next chapter.

![Diagram of flow regions.]

**Figure 2: Flow regions**

Region A is located upstream of the injection point where the net liquid flowrate out is equal to the condensation rate. The temperature profiles in this region suggest that the liquid never reaches saturated conditions and that there exists a recirculating zone. The driving force for this phenomenon may be the interfacial shear and the density effects due to large temperature gradients. This circulation zone may contribute substantially to the condensation process and can not be described by a 1D computer code. Moreover any further analysis on local phenomena will be altered by the lack of information on the amount of condensation in this zone.

Region B is concerned with the injection zone where condensation occurs on the jet itself and at the water surface. The temperature profiles
downstream the injection point show that most of the condensation occurs around the injection.

Assuming that the jet is continuous and does not break in droplets the Icikel correlation seems appropriate to predict the condensation on the jet. Nevertheless no experimental evidence exist to confirm this model:

\[ St = 0.00835 \left( \frac{L}{d_{is}} \right)^{-0.28} \frac{L}{d_{is}} \overline{Fr}^{-0.10} \]

where \( L \) is the jet length and \( Fr \) the Froude number,

\[ Fr = \frac{U_{is}^2}{g d_{is}} \]

The region C located downstream the injection zone exhibits a relatively flat interface with small temperature gradients since most of the condensation occurs around the ECC injection. The condensation process is controlled by the convective turbulent motion in the liquid phase. The Reynolds average turbulent diffusion approach predicts that the heat transfer coefficient may be expressed as:

\[ Nu_t = \frac{h_{is}}{k_1} = C Re_t^n Pr_{li}^m \]

where \( k_1 \) is the thermal conductivity of the liquid and \( Re_t \) is a turbulent Reynolds number based on turbulent length and velocity scales \( l_t \) and \( v_t \). Since the turbulence is generated at the wall and at the interface D. Bestion and L. Gros d'Aillon suggested that the turbulent velocity scale is a linear combination of the wall and interface friction velocities,

\[ v_t = a v_i + b v_w \]

The friction velocities are related to the liquid and gas velocities, \( v_i \) and \( v_g \) and the turbulent velocity scale determined to fit the experimental data,

\[ v_t = |v_i| + \sqrt{\frac{p_i}{p_g}} |v_g - v_i| \]

The final correlation has the form,

\[ Nu_t = C Re_t^{0.8} Pr_{li}^{0.4} \]
CONDENSATION IN THE VICINITY OF THE INJECTION

This section examines in detail the condensation around the injection. The experimental results show that a large fraction of the total condensation occur in this region. The condensation increase in the vicinity of the injection suggests that the turbulence induced by the jet is the controlling mechanism. A specific model has to be developed to describe the dependence of the turbulence intensity and diffusion on the injection line diameter, the injection velocity, the flow distribution and the pipe diameter. The model dependency on these parameters is critical during a scale up process from a small scale experiment to the reactor conditions.

A simple energy balance based on the liquid experimental temperature at the injection has been used to estimate the additional heat flux induced by the jet. The error in estimating this value may be large since it is based on a minimum temperature in the liquid but also because it assumes that the additional condensation due to the recirculating zone in region A is negligible. This experimental limitation is due to the lack of informations on the liquid velocity field.

The additional heat flux at the interface is expressed as a function of the exchange area, A, and the heat transfer coefficient, h,

\[ Q = A \cdot h \cdot \Delta T, \]

where \( \Delta T \) is the temperature difference between the steam and the subcooled water. The heat exchange area \( A \) defined above is not a simple characteristic of the system as it is in general for heat transfer correlations. In this particular case it depends on the injection and on the flow distribution. In terms of the Nusselt number the previous heat flux is,

\[ \frac{k}{l_t} = \frac{A}{l_t} \cdot \text{Nu} \cdot \Delta T, \]

where \( l_t \) is a turbulent length scale. Both the renewal theory and the Reynolds averaged turbulent diffusion approaches predict that the Nusselt number depends on a turbulent Reynolds number and on the liquid Prandtl number,

\[ \text{Nu} = \text{Re}_t^n \cdot \text{Pr}_t^m, \tag{1} \]

with

\[ \text{Re}_t = \frac{\rho_l v_t l_t}{\mu_l} \text{ and } \text{Pr}_t = \frac{\mu_l C_p l_t}{k_l}, \]
In general experimental measurements or theoretical studies are used to predict the turbulent velocity and length scales. During co-current and counter-current stratified flow many investigators used the wall and the interface friction velocities to evaluate the turbulent velocity scale. Since no theoretical model or experimental evidence is available for the turbulent scales associated with the turbulence created by a jet entering a water pool some assumptions are proposed to evaluate these quantities. Nevertheless further studies are necessary to understand the structure of the turbulent motion in this region.

Since no information is available on the turbulent velocity field we assume that the turbulent velocity scale of the large structure is directly related to the injection velocity, \( v_{is} \).

Two limiting cases are considered to estimate the turbulent length scale. First we assume that the jet diameter is sufficiently small or the pipe diameter large enough so that the system is similar to a jet entering an infinite water pool. In this case the turbulent length scale do not depend on the liquid height or the pipe diameter but is proportional to the injection line diameter, \( l_t \propto d_{is} \). A second limiting condition is reached when the liquid level is so low that the turbulent scale is controlled by the liquid height, \( l_t \propto h_1 \).

Nevertheless the test conditions in the COSI experimental program and large scale reactor safety studies suggest that in general the injection velocity is high enough to consider that the turbulent length scale is limited by the liquid height. This choice differs from the one proposed by D. Bestion and L. Gros d’Ailllon who considered the injection line diameter as the turbulent length scale. Finally we assume that the turbulent velocity and length scales are respectively \( v_{is} \) and \( h_1 \), but no experimental or theoretical evidence exist to confirm this choice.

In order to close the model we have to evaluate the heat exchange area. Two models are derived and compared with the experimental results. The infinite water pool limiting case heat exchange area depends on \( d_{is} \).

Nevertheless the injection velocity and the injection line diameter to pipe diameter ratio are in general both large enough to consider that the exchange area is limited in the transverse direction by the pipe wall. One way for these two assumptions to hold is to define the heat exchange area as follows,

\[
A = d_{is} \sqrt{\frac{1 - \alpha}{\alpha}} D.
\]  

where the transverse length is approximated by \( 2 \sqrt{\frac{1 - \alpha}{\alpha}} D \).

Another simple model is derived assuming that all the kinetic energy from the jet is dissipated according to the small scale energy dissipation mechanism. The volume of water considered is related to an axial length \( L \) and to the void fraction, \( \alpha \), while the kinetic energy flux depends on the injection velocity and diameter. The small-eddy model dissipation rate, \( \varepsilon \), depends on the turbulent velocity and length scale,

\[
\varepsilon = \frac{u_{is}^3}{h_1}.
\]
The resulting energy balance is,

\[ D^2 (1-\alpha) L t = \nu \varepsilon L d^2 \]

and the axial length of the exchange area is,

\[ \frac{d^2}{L} \]

We estimate the exchange area by approximating the transverse length by \( \sqrt{\alpha(1-\alpha)D} \) and it follows that,

\[ \Lambda = \frac{d^2}{\mu} \sqrt{1-\alpha} \alpha \]  \hspace{1cm} (3)

In order to compare the experimental results with the predictions of various models we define a Nusselt number based on the experimental value of the heat flux at the interface, the liquid temperature at the injection and by choosing one of the predicted heat exchange area presented above,

\[ \frac{Q_{\text{exp}}}{Nu} = \frac{k_1}{A h_1 \Delta T} \]

where \( Q_{\text{exp}} \) is the experimental heat flux, \( A \) the heat exchange area and \( h_1 \) the liquid height.

The comparison with the experimental results is made according to the Nusselt formulation presented in Eq.(1) with the Reynolds and Prandtl numbers defined as,

\[ Re = \frac{\rho_1 \nu_1 h_1}{\mu_1} \quad \text{and} \quad Pr = \frac{\mu_1 C_{pl}}{k_1} \]

Three different experimental test series are considered depending on the steam flow direction and the injection line diameter. During the "Direct" tests the steam flows as shown in Figure 1 and in the opposite way during the "Inverse" tests. The injection line diameter is equal to 5.6 mm for the "Small" test series and equal to 22 mm for the "Direct" and "Inverse" test series.

Different model comparisons with the Nusselt based on the heat exchange area defined in Eq.(2) are presented in Figure 3 to 6. The Nusselt number is plotted versus the predicted Reynolds and Prandtl numbers combination. The scales are not reported on the different graphs since we are looking for a qualitative agreement only and a straight line is shown to visualize the linearity of the data. Figure 3 and 4 suggest that the small eddy and large eddy models reviewed by Sonin et al. are not appropriate to describe the COSI experimental data.
Figure 3: Experimental versus predicted Nusselt number from the small eddy model

Figure 4: Experimental versus predicted Nusselt number from the large eddy model

On the other hand the model proposed by Kishinevsky and quoted by Sonin et al. predicts quite well the Nusselt number as shown in Figure 5. This result is not surprising since this model is intended for very high level of turbulent agitation with a fully turbulent diffusion into the liquid. Nevertheless the best fit to the experimental data presented in Figure 6 suggests no dependency on the Prandtl number.
Figure 5: Experimental versus predicted Nusselt number from Kishinevsky model

Figure 6: Experimental versus best fit Nusselt number with the heat exchange area defined in Eq.(2)

Finally the best result with the heat exchange area defined in Eq.(3) is shown in Figure 7 with the Nusselt number depending on the Reynolds number to the power 3/2 with no dependency on the Prandtl number.
The different models from the literature presented in Figure 3 to 5 do not seem to describe very well the physical mechanisms involved at the injection. Nevertheless the assumptions on the heat exchange area, the turbulence scales or the experimental heat flux may not be valid since they do not have a strong experimental or theoretical support. There is not enough information to conclude definitely on the condensation rate dependence on the injection velocity, the injection line diameter, the pipe diameter or the flow distribution. The correlations presented in Figure 6 and 7 are adequate at the COSI test facility conditions but not necessarily for different geometries and scales. These limitations are both experimental and numerical.

NON CONDENSABLE GAS EFFECT

A COSI test series was devoted to the study of the non-condensable gases effect on the condensation rate. Nitrogen was added to the steam flow with a mass quality between zero and about 1/3. The other parameters were unchanged. The experimental condensation rate is reduced by 38%. Data have been compared with the CATHARE code predictions based on the correlation presented by D. Bestion and L. Gros d'Aillon. In the model the interface is assumed to be at saturation temperature corresponding to the partial pressure of vapor and the nitrogen concentration is assumed homogeneous in the gas phase. The results are presented in Figure 8 where only 15% reduction of the condensation is predicted. It demonstrates that the presence of non-condensable gas does not change only the saturation condition but induces also a new resistance to the mass transfer. It is suggested that the condensing vapor entrains nitrogen to the interface creating a boundary layer where the non-condensable gas concentration is higher than the bulk value. The interface partial pressure of vapour is less than the average, the difference being controlled by the turbulent diffusion in the gas interfacial boundary layer. Modelling of this mass transfer process is possible using the classical analogy between the momentum and mass transfers. This effect is probably very important in case of very low turbulence in the gas (mass diffusivity is minimum) or in case of very high condensation (high mass flux).
CONCLUSIONS

The modelling of condensation at the ECC injection has to describe different physical situations.

The most difficult one is related to a liquid plug formation at the injection which may be stable or unstable. The modelling of such phenomenon requires the description of the important local mass transfer but also of the diffusion mechanism in the liquid plug that controls its behavior. Today the system codes can not describe properly these situations because it would necessitate the modelling of the axial turbulent diffusion and a very fine meshing.

At lower injection flowrate a more stable situation may occur with stratified flow in the cold leg. The condensation is still important around the injection but the modelling of this local effect is possible. Nevertheless some difficulties appear when extrapolating to new conditions with different geometries and scales. On the other hand the large temperature gradients in the liquid may induce density currents in horizontal pipes which can not be described by a one dimensional code.

The well established condensing flows may be modelled quite accurately using the large fundamental work available in this field. The turbulence created at the wall and at the interface controls the condensation. But as soon as a local perturbation generates additional turbulence the model is no more valid and a specific local modelling is required.

The modelling of non condensable gas presence seems possible but it is shown that the resistance to mass transfer in the gas must be taken into account. However there is a lack of data to assess the models in a large range of parameters.

Direct contact condensation is one of the few topics in which safety system codes must still improve their capabilities. The high thermal
nonequilibrium makes the situation very sensitive to any flow perturbation. Models may be valid for an experiment and not extrapolable to another one. Analysing LOBI tests B. Worth et al. report that some improvements of the RELAP 5 Mod1 standard condensation models were necessary. On the other hand the CATHARE 2 Rev5 models which were derived from GOSI data seem to overestimate the condensation in some LOBI tests. In the same way, Asaka et al. had to modify the current model of TRAC PFI Mod 1 to calculate correctly condensation in combined injection tests performed in CCTF.

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MULTI-PHASE FLOW ASPECTS OF FUEL-COOLANT INTERACTIONS
IN REACTOR SAFETY RESEARCH

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ABSTRACT

The energetic FCI has long been recognized as an industrial hazard, and more recently has been considered as a possible hazard during a severe accident in a nuclear power plant. The focus of this paper is on the latter application with specific emphasis on in-vessel and ex-vessel situations in which molten fuel may come into contact with the water coolant. Our focus is twofold; first, to explain the rationale for current research into FCIs and second, to discuss the important multi-phase flow issues that arise from such investigations. After the many years of research on energetic FCIs there still appears to be three areas where the FCI is important to consider: 1) fuel melt quenching in a water pool, 2) adding water to a degraded core, and 3) FCI energetics. Under current agreements these areas are being actively investigated by researchers in the European community as well as the United States. Such experiments with international cooperation are briefly discussed (e.g., FARO, KROTOS and MACE). In such experiments difficulties arise in measuring the appropriate quantities to characterize the FCI phenomena due to the high transient nature of processes involved. We discuss the important multi-phase flow topics in which further basic research may be needed to aid in FCI model validation, and how these subjects relate to the FCI.

1. INTRODUCTION

The energetic fuel-coolant interaction (FCI sometimes called a vapor explosion) has been a recognized industrial hazard since the 1950's. More recently in the early 1970's, WASH-1400 (1975) considered a vapor-explosion-induced missile as a possible mode of direct containment failure (i.e. alpha-mode). Throughout this time research has been conducted to be able to cope with the hazard in industrial applications or to assess its risk given an industrial accident. A complimentary part of these investigations has always been that one may learn more about the fundamental mechanisms. However, the details of the process under various geometries has eluded complete understanding or the ability to predict its
destructive magnitude reliably. A review has been done on these phenomena (Corradini, 1988, 1991).

We now enter the 1990's and the focus of some reactor safety research is again on aspects of the FCI, energetic or relatively benign. The purpose of this paper is to explain the rationale for the current work in FCI phenomena and under what conditions the FCI is important for reactor safety risk or severe accident management. In addition, we focus on the important multi-phase flow topics that require further research to aid in our fundamental understanding. To do this we first briefly describe what an FCI characteristics might be and then discuss the three areas where it has importance today and finally some specific multi-phase flow topics that require more research.

2. FUEL-COOLANT INTERACTIONS AND SEVERE ACCIDENTS

A vapor explosion (sometimes called an energetic fuel-coolant interaction, FCI) is a process in which a hot liquid (fuel) transfers its internal energy to a colder, more volatile liquid (coolant); in doing so the coolant vaporizes at high pressures and expands, doing work on its surroundings. Consider a qualitative description of the mechanistic path (Board et al., 1974a, 1974b) by which the stored fuel internal energy is converted to produce work by a high pressure vapor. In a typical vapor explosion when the two liquids first come into contact, the coolant begins to vaporize at the fuel-coolant liquid interface as a vapor film separates the two liquids. The system remains in this nonexplosive metastable state for a delay period ranging from a few milliseconds up to a few seconds. During this time the fuel and coolant liquid intermix due to density and velocity differences as well as vapor production.

Then vapor film destabilization occurs, triggering fine fuel fragmentation. This rapidly increases the fuel surface area, vaporizing more coolant liquid and increasing the local vapor pressure. This "explosive" vapor formation spatially propagates throughout the fuel-coolant mixture causing the macroscopic region to become pressurized by the coolant vapor. Subsequently, the high pressure coolant vapor expands against the inertial constraint of the surroundings and the mixture itself. The vapor explosion process is now complete, transforming the fuel internal energy into the kinetic energy of the mixture and its surroundings. This kinetic energy takes two forms. At early stages shock waves can be generated in the fuel-coolant mixture and at later times the overall mixture is accelerated by the expanding coolant vapor. The high pressure vapor produced, the dynamic liquid phase shock waves, and the slug kinetic energy can all do destructive work on the surroundings. In addition if the fuel is metallic this explosive dispersal may cause exothermic metal-water chemical reactions which can produce hydrogen and might enhance the work output.

To be more precise the vapor explosion can be considered as a subset of any fuel-coolant interaction in which the timescale for heat transfer between the liquids is smaller than the timescale for pressure wave propagation and expansion in a local region of the fuel-coolant...
mixture. Therefore, the rise in pressure locally forms a shock wave, which spatially propagates with a velocity which is greater than the characteristic speed of sound in the mixture ahead of the shock front. The key feature of the vapor explosion is that the shock wave propagation through the mixture drives the rapid fuel fragmentation and associated heat transfer to the coolant, i.e., analogous to shock heating in a chemical detonation.

In general a fuel-coolant interaction does not exhibit these shock wave characteristics. Thus fuel fragmentation is not necessarily linked to shock wave propagation and the rapid boiling phenomena does not spatially propagate on a timescale equal to pressure wave propagation. A large amount of coolant vapor may be produced in this process and the fuel may still become finely fragmented, yet the character of the fuel-coolant interaction is not explosive. One should note that analogous to a deflagration such an event might still be destructive under certain conditions.

In present day nuclear fission reactors if complete and prolonged failure of normal and emergency coolant flow occurs fission product decay heat could cause melting of the reactor fuel. If a sufficiently large mass of molten fuel mixes with the coolant and a vapor explosion results, the subsequent vapor expansion might cause a breach in the containment of the radioactive fission products by dynamic or static pressurization or missile generation caused by the slug kinetic energy. Although this type of severe accident is considered highly improbable, the health consequences are large enough that it is considered in safety studies.

One should also realize that in commercial LWRs prompt-burst accidents can be excluded, thus we are only concerned with initially segregated fuel-coolant configurations (i.e., contact modes), in which the fuel or coolant is "stratified" from the other. The pouring mode of contact can be obtained if the fuel contacts the coolant by "pouring" into it. This mode of contact can be obtained if the fuel melt in the core region breaches its crust boundary and pours into the lower plenum water pool or if the fuel breaches the reactor pressure vessel (RPV) and pours into the cavity below. The former case was observed at TMI and the fuel eventually quenched. In contrast, a stratified geometry may occur if water is poured atop the fuel in-vessel or ex-vessel. Also, a benign FCI with inadequate quenching in the "pouring mode" would revert to a stratified configuration; again as occurred in TMI.

3. CURRENT FCI ISSUES

The focus of current reactor safety research with regard to severe accidents has gone beyond risk assessment to applications in accident management as well as consideration of passive safety features in advanced LWR designs. Because of this the nature of the information required has changed and past research results may be inadequate to provide answers to certain issues. There are three specific issues that require additional information either from experimentation or by analysis of fuel-coolant interactions:

1) Fuel melt quenching in a water pool,
2) Adding water to a degraded core,  
3) Energetics of the FCI.

3.1 Fuel Melt Quenching

The TMI-2 accident indicated that under certain conditions the fuel melt may be quenched at that time of pouring into a water pool in the RPV lower plenum. Previously it had been assumed that a fuel pour into the RPV lower plenum would result in either settling of the fuel unquenched (and eventual RPV wall failure) or a vapor explosion. Although there has been a great deal of integral FCI experiments there is no data under these particular conditions. Because of this lack of data the FARO-LWR experiments are planned (Fasoli-Stella et al., 1991). They involve a prototypic fuel mass (50-150 kg of UO$_2$/ZrO$_2$/Zr@ 3000K) poured into saturated water at high pressures (5-50 bar for 1-2 meters depth with a vessel diameter of 0.5 to 0.7 m).

The objective of these tests to be performed at the Joint Research Center in Ispra is to observe the integral behavior of fuel melt quenching at high pressures under likely severe accident conditions. What makes these experiments especially attractive and compelling from a technical standpoint is that they can be performed with real reactor materials (UO$_2$, ZrO$_2$, Zr) at temperatures and pressures that are prototypic of actual severe accident conditions and with the proper full scale water depths for in-vessel accident situations. It should be noted that the FARO-LWR experiments are not vapor explosion experiments, although explosions might occur and the facility would accommodate them. Also it is generally agreed that the FARO-LWR experiments are not specifically benchmark experiments to be used primarily for code validation. Nevertheless, the instrumentation within the FARO-TERMOS facility is substantial and the data collected is extensive. This it is likely that computer code comparisons will be made to gain modelling insights into fuel melt mixing and melt quenching in water. This will be considered again in a subsequent section.

Because these experiments are using prototypic materials under realistic initial and boundary conditions at the proper vertical length scales (e.g., water depth) the question of scale only becomes an issue relative to the size of the fuel melt pour and the lateral dimension of the facility. The FARO facility has the capability of delivering a large mass of oxide melt under a variety of conditions. It is planned that the fuel pour rate can be within reasonable ranges for accident conditions, i.e. jet diameters 5-10 cm and entry velocities of a few meters per second.

Second, the FARO experiments could be considered as representative of two types of geometric situations:
1) a single jet in a large water pool, or
2) a unit cell of a multiple jet pour into the lower plenum.

In either case the adequacy of the FARO facility vessel to provide a properly scaled lateral dimension depends to some extent on the degree of melt quenching. It is important to note
that the vessel cross-sectional area will be varied by over a factor of two in the tests to be performed to specifically address this point. If the results of a scoping test (50 kg of melt) and the base case experiment (150 kg of melt) indicates that melt quenching is minimal (e.g., <10% of the fuel quenched during the pour), then the steaming rate will be low, level swell minimized and the steam superficial velocity small. Under these conditions the lateral dimension of the vessel will not be an important concern regardless of which scenario one may want to consider. Conversely if the results of the scoping test and the base case experiment indicate significant melt quenching (~100% of fuel melt quenched during the pour) then the scaling of the experiments considering the lateral dimension will be problematic for either scenario. Qualitatively one would still expect the experiments to be quite informative and valid for reactor safety implications. However, quantitative interpretations of the tests must then account for the lateral dimensions and the likely large steaming rate, superficial velocity and all its consequences (e.g., substantial fuel and liquid water sweepout with the steam).

Based upon these considerations it seems clear that once the scoping test and the base case test are performed in the FARO-LWR experimental program, one should reassess what is the scaling rationale and what would be the important parameters to investigate in the future experiments. Currently eight experiments are planned for the test series.

3.2 Adding Water to a Degraded Core

Severe accident management is a natural outgrowth of past emphasis on risk assessment. In fact, there are a number of particular issues that must be addressed when accident management is the main objective. "Timing" is important to accident management and only recently have PRA studies considered it in some rudimentary fashion; e.g., NUREG-1150 (1990) considered the effect of operation of engineered safety features (containment fan coolers and/or sprays) before core heatup, before vessel failure or after failure. Inclusion of this "timing" behaviour can indicate where opportunity exists for operator intervention to help reach a stable coolable state. Since water is the primary accident management tool and the FCI can alter the course of the accident it is important to investigate the benefits of adding water to the degraded core with consideration of the possible adverse "side-effects". This is particularly true because many of the fundamental mechanisms are not well understood.

The current research approach to this issue is to review past investigations in which water (or its simulant) has been added to a degraded core and determine what the adverse effects could be and what the current state of knowledge (data and analysis) suggest. The working hypothesis is that adding water to the degraded core is a benefit under all circumstances, but one must try to quantify the possible adverse effects, and minimize their impact; such as:

1) possibility of recriticality under certain core reflood accident conditions,
2) hydrogen generation and initial fuel heatup due to exothermic metal-water reactions,
3) energetic FCIs adversely affecting the attainment of a stable coolable state.
The major variables affecting if water addition would have adverse effects would be the rate and character of water addition and the reference state of the fuel at the time it is added. To help in focusing this work qualitative scenarios of the accident are developed with reference to water availability and its effect. The reference states for the fuel on the basis of qualitatively different fuel-coolant contact configurations are:

1) initial heatup and core degradation (rods "intact"),
2) advanced core degradation (core rubble, melt and relocation),
3) core relocation and slumping with the lower plenum,
4) ex-vessel fuel-coolant interactions.

An initial review of past experiments suggests that a few tests have already been performed as part of the CORA and PBF experimental program as well as the LOFT-FP2 test. Also past simulant tests of a coolant added to fuel debris have been performed at Brookhaven, Argonne and UCLA. Although limited in scope these tests address water addition during core degradation in-vessel. The major parameters observed in all of these tests was hydrogen production, as well as fission product release. Although these phenomena are quite important to severe accident phenomena they do not directly impact FCI issues, and are not discussed further. The TMI accident is also relevant to fuel melt relocation and quenching. Also it is expected that future FARO-LWR test data can address this geometry.

Ex-vessel the only data planned to be provided by the MACE and WETCOR experiments (Sehgal et al., 1991; Copus, 1991). These latter experiments are large-scale ex-vessel molten fuel-concrete interaction tests involving water addition, sponsored by the ACE International consortium and the NRC respectively.

It is also recognized that following an energetic FCI the course of the accident may be altered, due to finer fuel debris, rapid steam production and possibly hydrogen. For these reasons a review of past FCI data and models is important to focus on the ability to predict or at least to bound the effects of the FCI during various times in the accident coupled with new data from the FCI energetics experiments discussed below.

3.3 FCI Energetics

The final issue that must be considered is the general question of under what conditions must vapor explosion energetics be considered and what are reasonable estimates for the energetic yield.

A comprehensive risk assessment in 1975 in WASH-1400 was the first to estimate the likelihood of early containment failure by a vapor explosion. This study focused on two specific reactor designs: the Surry PWR and the Peach Bottom BWR-Mark I. For the vapor explosion process, it was determined that the containment could be threatened by three possible damage mechanisms:

1) dynamic liquid phase pressures on structures,
2) static overpressurization of the containment by steam production, and
3) a solid missile generated from the impact of a liquid slug accelerated by the vapor explosion.

Rapid hydrogen production due to metal-water reactions was not considered due to estimates of large amounts of in-vessel clad oxidation during core heatup-degradation. Based on analyses, it was determined that the primary concern was a direct failure of containment caused by an energetic FCI in-vessel causing missile generation (designated "alpha-mode" failure).

In a more recent review in 1985 the probability of alpha-mode containment failure was also subjectively estimated by the Steam Explosion Review Group (SERG, 1985). This group of experts performed independent analyses and examined available experimental data to arrive at their opinions. The spectrum of their opinions indicated that the conditional probability of alpha-mode failure is considered to be much less likely than in WASH-1400 (10^{-2}-10^{-4}/yr as upper bound given a core melt). This group also recognized that these estimates were founded on the judgement that the amount of fuel-coolant mixing was limited and/or the explosion yield was less than maximum thermodynamic values. Included in their findings was the consensus recommendation that fundamental experiments be performed at intermediate scales (masses less than 100 kg) to characterize fuel-coolant mixing and measure explosion yield as well as the effect of mixing on yield. This is the major reason for these additional research efforts.

A few experimental efforts have now begun to address these issues. First, large scale experiments on fuel-coolant mixing at UCSB are planned where heated stainless steel spheres (fuel simulant) are poured into a water pool which can be made geometrically similar to the reactor lower plenum or cavity. The use of these materials allows one to visually observe the global development of the mixing zone as well as make measurements of local void fractions with more advanced instrumentation being developed by UCSB (Theofanous et al., 1991). The hypothesis for these experiments is that mixing will be limited by local steam formation and high void fraction causing water removal from within the fuel-coolant mixture. With such data one can compare the experimental results to computational models (e.g., IFCE or PM-ALPHA, Young, 1985; Theofanous et al., 1987) which have been used as part of the safety case for suggesting limited mixing at the reactor scale.

A second set of vapor explosion experiments are underway at JRC Ispra and being planned at the University of Wisconsin. The purpose of these experiments is to produce a well-controlled one-dimensional geometry in which a fuel simulant (e.g., tin at 2-20 kg, 1300K) pours into a water column, mixes with the coolant, an explosion is triggered and the explosion expansion work measured. These experiments are aimed at providing benchmark data to examine the effect of fuel-coolant initial conditions and mixing on explosion energetics. The hypothesis for these tests is that fuel-coolant mixing conditions directly limit energetics below maximum thermodynamic bounds. Data is needed where mixing and explosion processes occur under controlled conditions. With such data one can compare fuel coolant mixing and explosion models used to make the safety case at reactor scale to
these experiments. There is also an experimental program, KROTOS, at the JRC Ispra that investigates the energetic FCI in a well-controlled one-dimensional geometry. This apparatus has the advantage of being able to use a wider range of fuel simulators (metals: Sn, Al, Zr; oxides: Al₂O₃, UO₂, ZrO₂).

It should be realized that the major hazard from a vapor explosion still revolves around its energetics and how the energetic FCI may threaten containment. For in-vessel situations this logically translates into the alpha-mode failure issue. For ex-vessel situations the importance of the vapor explosion very much depends on the particular geometry and the initial/boundary conditions. For example, the sensitivity calculations in NUREG-1150 suggested that ex-vessel explosions in a Mark-II and Mark-III drywell may lead to drywell failure. However, more detailed analysis remains to be done to verify that this threat exists.

4. TRANSIENT MULTI-PHASE FLOW TOPICS IN THE FCI

In all the current FCI research areas discussed the experiments conducted rely on the measurement of parameters which depict the integral behavior of the FCI. The reason for this approach is two-fold. First, the experiments are complex involving physical processes which are highly transient. Therefore, it is just as important to determine the qualitative and quantitative trends of the phenomena as well as the detailed results. Observation and measurement of the integral FCI behavior is quite useful for this purpose. Second, the measurement of detailed parameters, such as particle diameters, velocities, temperatures and component volume fractions, in such a highly transient multi-phase is difficult at best and cannot be solely relied on to provide the needed data to quantify the phenomenon. Measurement of these detailed parameters is fundamentally a separate research effort. Thus integral quantities are the first measurements needed and more sophisticated integral or detailed local measurements require more research and development. This section focuses on the three FCI research areas to identify where such work is necessary.

The major objective of the melt quenching experiments is to determine the amount of fuel quenched "in-flight", i.e., as the fuel pours through coolant pool. In the FARO tests this is accomplished by measuring the pressure rise in the closed volume tests (compensating for condensation) or by measuring the integral amount of steam generated in tests where a constant pressure is approximately maintained. Measurements of gas phase pressure and mass flow are required and can be accomplished using state-of-the-art techniques. Such measurements are checked by examination of the post-test fuel debris to determine the amount of fuel breakup. Finally, the heat load to the basemat on which the debris rests is determined by thermocouples within the pool, the fuel debris and basemat material. Even if these integral measurements are completely successful more detailed measurements of the multi-phase flow would be desirable to verify the integral measurements and understand the details of the flow. This is also important because modelling of the phenomena would require more detailed knowledge for validation of the predictions. In our view three aspects of melt quenching phenomena require more detailed measurements:
- void fraction in the pool,
- fuel particle evaluation during mixing,
- effect of subcooled conditions on quenching.

The first topic of void fraction is important in order to verify that any model can successfully predict the local effect of fuel-coolant heat transfer (in the absence of an explosion). One way to determine this is to measure the integral transient level swell of the pool; not necessarily a straightforward measurement. Another important measurement to characterize fuel-coolant heat transfer is the fuel particle size as it breaks up from the main pouring jet. This has not been successfully done in a multi-phase flow although it has been accomplished for two-component flow and is useful for model validation. Finally, melt quenching can occur in subcooled pools. Such a question has not been fully investigated and again is a key determinant in liquid-liquid heat transfer.

The objective of investigations involving adding water to a degraded core centers around the ability of the water added to extract the sensible heat of the melt ("quench it") and remove the decay heat over a longer time (coolable state). As mentioned previously, hydrogen generation and fission product release are aspects that are not directly addressed in this discussion. The upward heat loss from the fuel debris pool and its ultimate state are also important. Current tests (e.g., MACE) focus on the ex-vessel situation where a fuel melt pool is atop a concrete basement as water is added above it. The integral measurement of the upward heat flux is the key observable with the post-tests debris again used as an indication of the fuel debris morphology and the presence of any solid crusts indicates that water contact with the fuel pool may be prevented. The contact of the molten fuel debris with the water pool in this situation is affected by the void fraction of the fuel pool due to concrete decomposition gases, the solidified fuel debris morphology and once again the enhanced benefit of subcooled boiling on fuel-coolant heat transfer.

The final area of FCI energetics is different because the objective is to determine the mechanical energy yield from an explosion. This means that the dynamic pressures from the explosion (liquid phase) and quasi-static pressures (vapor-phase) are measured along with the kinetic energy (and/or kinematics) of the explosion fragments. As discussed in the previous section because it is our hypothesis that fuel-coolant mixing is the important determinant in the explosion yield the same detailed measurements discussed previously are important here to determine the details of the mixing conditions.

4.1 Void Fraction and Level Swell Measurements

Measurement of void fraction and level swell for a pool of liquid with vapor generation or gas injection has been a common topic for development of empirical void correlations under steady-state conditions (Ishii, 1991). The modelling approach has been to correlate the data using a drift flux model. More recently, Casas et al. (1991) has extended the database to non-aqueous liquid pools as might be important for ex-vessel debris coolability and molten-core-concrete interactions. In these applications visual observation of level swell (or
mechanical measurement of it by a movable float) or measurement of void fraction by differential pressures over a distance were quite useful in measuring the integral void fraction. Local measurement of void has also been successful in particular applications (Ishii, 1991).

However, in all the situations common in FCI experiments the level swells rapidly (<1 sec) and the void fraction behavior can be different from steady-state cases. Casas (1990) noted in his work that during establishment of steady-state conditions the void would rise above steady values (>60% void) particularly in the bubble-churn transition until bubble coalescence causes the void to decrease. More work is needed in the development of integral techniques of void measurement that can resolve changes in void over short time-scales. Such development is underway in the instrumentation development for FARO and KROTOS. A new measurement techniques (designated FLUTE) has been developed by Theofanous and coworkers (Theofanous, 1991) in which a local probe is capable of measuring the liquid fraction due to the fluorescence of an additive in the liquid. Preliminary results are promising indicating that liquid fractions are low in the central region of the mixture of simulant fuel spheres and a water coolant.

4.2 Particle Sizes of a Dispersed Phase in a Fluid

In many applications it is important to determine the size of the dispersed phase of particles in a continuous fluid; e.g., jet behavior in combustion or spray atomization. As in the case of void fraction measurements both integral and local quantities are measured. The integral behavior of the jet in such applications is analogous to the pouring contact mode of the FCI and kinematics are used to measure the jet breakup length (Bower et al., 1988) or the jet spray spread angle. Also local measurements have been made of particle sizes using LDV techniques (Reitz, 1989). More recently, development of advanced instrumentation and electronics have extended the LDV technique to develop the Phase Doppler Particle Analyzer (PDPA) in which size and velocity of droplet particles can be measured in the dilute breakup region of a transient liquid jet spray in a gas (Koo, 1991). The major limitation of such an optical technique for this multi-phase application is that the vapor and particle phases will both scatter the laser light making this technique not directly applicable. Some techniques must be developed to account for multiple scattering events.

The evolution of the fuel particle size is probably one of the most important local parameters to measure since it determines the fuel interfacial area which helps determine the heat transfer to the coolant and the vapor production rate. The concept of using some optical technique to measure the particle size of the fuel within the coolant may be possible if development is continued. Most recently, El-Beshbeeshy (1992) has used the attenuation of visible light through a jet spray/gas mixture to measure the Sauter Mean Diameter of the spray in dilute and relatively optically dense regions. Such a technique could be theoretically used in a multi-phase application with development of illumination schemes and corrections for multiple scattering events. This is a fruitful area for experimental multi-phase research and associated modeling of transient jet breakup.
4.3 Subcooled Film Boiling

A likely situation to occur during a severe accident is the contact of the molten fuel with a subcooled coolant pool. The fuel temperature would be large enough that stable film boiling would occur even at relatively large subcoolings (−100°C). In such a situation the heat transfer coefficient between the hot surface and the subcooled pool is not well known, because the data is quite sparse (Dhir, 1978; Aziz, 1989). This heat transfer coefficient is important to determine because it greatly influences the energy split between the heat transferred to the bulk coolant and that heat which goes into vapor production at the interface. This latter heat transport path determines the local void fraction and thus the fuel-coolant mixture conditions. Currently, Theofanous (Angelini et al., 1991) is investigating this film boiling regime in the development of the FLUTE measurement technique. Also subcooled film boiling is being studied in an extension of Dhir’s original experiments in a spherical geometry (Konsella, 1991).

5. CONCLUSION

The FCI is an event which still remains somewhat of a mystery as to its precise mechanisms and the interaction of particular stages of the process. In relation to current regulatory issues there are three areas where further work needs to be focused:
- fuel melt quenching in a coolant pool,
- adding water to a degraded core and core coolability,
- FCI energetics.

When investigating these issues it appears that there are fundamental topics in transient multi-phase flow that require more understanding to progress in the understanding of the overall FCI process. Three of these have been discussed:
- void fraction and level swell measurements,
- particle sizes of a dispersed phase in a fluid,
- subcooled film boiling.

In each area more precise experimental measurements are needed combined with a better knowledge of instrumentation and modelling of multi-phase systems.

6. REFERENCES


CAPABILITIES AND LIMITATIONS OF

THERMALHYDRAULIC CODES

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ABSTRACT

Advanced thermalhydraulic codes such as CATHARE, RELAP 5 or TRAC are now part of the everyday tools used for Pressurized Water Reactor safety studies. In the last few years methodologies for code uncertainty evaluation have been developed. The relevance of these methods is still under discussion and it may be argued that they do not fully take into account all the inherent limitations of such codes. In relation to this problem, it is useful to get a clear view of the capabilities and limitations of a thermalhydraulic code. As a contribution to this work some conclusions from the analyses of the CATHARE code assessment calculations are presented.

The first part of this paper considers aspects of the code development process in order to keep in mind the successive simplifying assumptions required to get the final model. This process includes the derivation of averaged balance equations from exact local instantaneous equations, the choice or development of the constitutive relations, the discretization and the solution methods. Each of these steps is associated with a degradation of the physical description. Examples are given which illustrate some limitations arising from the treatment of the momentum balance equations.

Next a review of code prediction deficiencies during calculations of analytical tests is presented leading to conclusions on the accuracy and range of validity of some constitutive relations. Analysing the mispredictions of integral tests, various kinds of difficulties have been pointed out. At first, problems may be related to a questionable choice of schematization. Some examples are given which show that a precognition of the transient is sometimes necessary to eliminate this particular user effect. Limitations are also faced when a constitutive relation is used out of its validity range. A more critical case is encountered when a specific physical situation has not been yet studied in analytical tests and is not described by any of the models. Finally some transients may themselves be highly sensitive to certain physical processes. This paper focusses on problems encountered in predicting core swell level, flooding limit and CCFL in complex geometries, loop seal clearing, condensation at ECC injection and gravity driven reflooding.
INTRODUCTION

The object of this paper is a discussion about the reliability of thermalhydraulic codes used for Pressurized Water Reactor safety. Ideas developed here are based mainly on the experience of the CATHARE code development and assessment. It is believed that they are mostly applicable to the other 2-fluid best estimate codes like RELAP 5 and TRAC since these codes have many similarities. As there are no more conservative assumptions in these codes, the question of the uncertainty arose and led to important developments within the past few years. Methodologies are based on sensitivity tests. A transient is calculated several times with modifications of some parameters which are considered to be possibly sensitive. It requires an expertise to select the list of parameters to change. The non linear nature of the basic equations makes the problem difficult and present methods are very heavy. They must not only quantify the uncertainties due to initial conditions, physical properties, closure relations, but they should also take into account physical processes which may take place in the transient and are not modelled in the code. This paper contributes to list the code limitations which affect the most predictions of reactor accidental transients. Such a work must be included in the uncertainty analysis in order to get a complete view of the code reliability. Two successive point of view are adopted to draw the code limitations:

- First the point of view of the developer gives an a priori estimation of limitations due to the assumptions and simplifications made to obtain the final physical and numerical model. This approach remains qualitative but gives a very pessimistic view of the code capabilities.

- Then the assessment calculations of both separate effect tests and integral tests are analyzed to determine the origin of the main discrepancies. Several types of errors are distinguished. Some of them are related to shortcomings of the model listed by the developer. Nevertheless this point of view will make the reader more optimistic.

Some examples of code mispredictions are presented to illustrate the limitations of the present version of the CATHARE code.

1 THE POINT OF VIEW OF THE CODE DEVELOPER

1.1 Derivation of the set of equations

Advanced thermalhydraulic codes use the two fluid model where mass momentum and energy balance equations are written for each phase. These equations can be derived from exact local instantaneous equations. As described in ref (1) or (2) the process includes several steps: space and time averaging, simplifications through physical assumptions, derivation of closure relations. Models are restricted to zero order closure so that no more p.d.e equations are derived.

1.2 The averaging procedure

The averaging process which restricts predictions to large scale phenomena is necessary to allow reasonably coarse meshing and to make comparison with experiment easier. The time integration or averaging suppresses from calculated quantities fluctuations due to the turbulent nature of the flows. The space averaging is also very helpful in two phase flows as it allows to forget the complex structure of phase repartition and interface movements. The effects of small scale processes on macroscopic evolution can be taken into account by appropriate closure relation. However the time and space
scales at which fluctuations can be filtered is not always evident. It is clear when the spectrum is cut into two separate zones, the range of small scale processes and the macroscopic range. Dispersed flows are generally in this favorable case. But there are also situations with a continuous spectrum and possible non linear interactions between the larger and smaller scale processes. Should for instance the intermittent nature of slug flows be filtered or described in the mean flow? Filtering will not be a limitation if this intermittency does not interact with important physical processes like a wall dry out for example. The domain of integration may also be troublesome when it puts together things which are too different. For example in an annular flow with droplet entrainment there are two liquid fields which have very different velocities and very different types of interaction with the walls and the gas phase. In summary the averaging induces a limitation when it masks small scale physical processes which have strong non linear interactions with the macroscopic scales.

1.3 Simplifying assumptions

The current simplifying assumptions are:

- In 1-D models the axial diffusion of heat and momentum by molecular diffusivity or by turbulence is neglected. Moreover all the correlation coefficients due to space averaging are taken equal to 1 by simple lack of knowledge. The loss of information associated with this simplification can be partly restored by an appropriate modelling of the transverse momentum and heat fluxes. This is possible when the transverse profiles follow a similar or affine solution. But in cases where the profiles are rapidly changing, the simplification cannot be justified. Then best accuracy can be expected in the description of established flows in long pipes without singularities.

- In 2-D or 3-D models only diffusion towards walls or interfaces is correlated. The internal turbulent diffusion inside each phase is not modelled. A more complete diffusion modelling is possible with some limitations. The meshing must be fine and the numerical scheme must not be too diffusive. Moreover, considering the state of the art in turbulence modelling in two phase flows, the present knowledge is limited to dispersed flows.

1.4 Closure relations in a 1-D model

Closure relations were extensively studied in the frame of the 1-D model. Many separate effect experiments have been analyzed to determine constitutive relations concerning mass momentum and energy transfers between phases or between fluid and walls. The difficulties come from the large variety of situations to deal with: variety of geometrical configurations, variety of flow patterns, variety of heat transfer modes, large range of thermal-hydraulic parameters.

Constitutive relations are essentially algebraic expressions of the principal variables. In the CATHARE 1-D model only two differential terms are present in the interfacial momentum transfers: the added mass term associated with inertial effects has been derived for dispersed flows and another term proportionnal to the void fraction gradient plays an important role in stratified flows. Apart from these two terms, algebraic closure relations are developed on the basis of steady and established flows (or quasi steady and quasi established flows). In unsteady or non established flows it is implicitly assumed that this closure is still valid. This assumption may be the source of some shortcomings, particularly if one considers the physical processes which have long relaxation times. Algebraic closure means that all the physical mechanisms involved have a relaxation time equal to zero. In 1-D models all the transverse profiles are considered as
established even at the entrance of a pipe. It is then obvious that flows in pipes or ducts with a high length to diameter ratio L/D are easier to describe correctly.

1.5 Flow pattern maps in codes

Every flow regime has its internal structure and its transfer mechanisms. So it seems natural to use a flow pattern map in a code and to develop correlations for mass momentum and energy transfers which depend on the flow pattern. Unfortunately at present there is not a universal map valid in the whole domain of simulation. Experiments with steam water at high pressure and in large diameter pipes are very expensive and observations very difficult. So the available flow pattern maps are not validated in this domain. Moreover it is not absolutely necessary to determine all the transitions and to use specific correlation for each flow regime. In the CATHARE code only the onset of droplet entrainment and the stratification criterion are explicitly written. These two transitions are important because they limit a separated flow and a more dispersed flow. Anyway closure relations can also be expressed directly as functions of the principal variables without reference to a particular flow pattern. So the absence of a unique and general flow pattern map in the codes is not a limitation by itself, but it reflects the limits of the physical knowledge in two phase flows.

1.6 Phenomenological and empirical correlations

A purely empirical correlation is a best fit of experimental data where the quantity to model is expressed as any function of the principal variables. It can be very accurate within the domain of experimental investigation but the extrapolation beyond it is very dangerous. On the other hand this method does not take any benefit from the knowledge which may exist in certain subdomains where good correlations are available. Dimensional analysis allows in principle to determine the dimensionless numbers to use in the expression of the quantity to correlate. But in 2-phase conditions the number of independent parameters is very high so that simplifying assumptions are necessary. When the controlling physical processes are well identified one can keep only the few dimensionless parameters which play a role. In this case the extrapolation beyond the investigated domain is less hazardous. Nevertheless there is no guarantee since the controlling processes can be different in an other range of parameters. For example slug flow does not exist any more in large diameter pipes. The phenomenological or mechanistic approach consists in assuming a governing physical mechanism. The correlation is then derived theoretically without anything coming from experiments. An alternative is to keep some free parameters to adjust to data. Even with this last precaution the extrapolation beyond the qualified domain is not reliable. New effects which are not present in the model may become important in another range of parameters. The experience shows that the 2-phase thermalhydraulics contains myriads of phenomena which make it difficult to generalize any theoretical breakthrough.

1.7 Closure relations in 2-D or 3-D models

Closure relations used in 3-D models are generally extrapolated from 1-D models. This may lead to important shortcomings as quantities averaged over the cross section of a duct have not the same meaning than local values. For example the void fraction is an important indication for the determination of the flow pattern in a 1-D model when it is not in a 3-D model.
The main problem is associated with the lack of turbulent diffusion modelling in present 2-D or 3-D models implemented in system codes (TRAC,CATHARE). These models should be used only when the turbulent diffusion effects are dominated by other effects. A first example is the core, a very porous medium where the diffusion towards rod walls or interfaces is much higher than the large scale turbulent diffusion. Moreover in low velocity two phase conditions gravity effects are likely to produce the most important large scale mixing effects. The lack of diffusion terms is not restrictive in this case.

The annular downcomer is represented either by a two dimensional component (particularly for the refill phase of a large break LOCA where the asymmetrical behaviour creates strong horizontal velocities) or by assembling one dimensional elements. It is clear that a 2-D calculation provides at least a better representation of inertial effects which are important in the LBLOCA. But it is difficult to decide which effect is the most important between interfacial friction, which is modelled and turbulent diffusion, which is not modelled. Only an extensive validation on UPTF scale 1 downcomer refill tests can give some confidence on the predictions of such a two 2-D model.

The closure of multidimensional two phase flow models is still in its infancy. A tremendous lot of work is still required to reach the same quality as the 1-D models have, since there are much more physical processes to describe and more terms in the equations to correlate. So they must be used with caution only where and when 2-D or 3-D effects are important and when their limitations are not critical.

1.8 The problem of singularities

The presence of geometrical singularities in a circuit, such as bends, flow area contraction or enlargement is a difficulty for the two phase flow models. At these locations the flow is perturbed and closure relations obtained in quasi established flows are not justified. As the flow structure is affected, perturbations may concern all the physical processes such as momentum exchanges, heat and mass transfers. The turbulence is generally increased, giving enhanced heat and mass transfers as well as irreversible pressure drops. Unfortunately, these local effects depend on many geometrical parameters and no general modelling can be proposed. Each case should be studied separately.

1.9 The problem of scale extrapolation

Analytical tests are generally limited in scale. Then the closure relations cannot be correctly validated at the reactor scale. As mentioned previously, flow pattern maps are not verified in large diameter pipes. Even the use of phenomenological constitutive relations does not guarantee the extrapolation. Moreover in reactor circuits the length to diameter ratios are often smaller than in small scale analytical tests. This makes the problems of flow establishment more critical and gives more weight to the flow perturbations at the geometrical singularities. Larger scale may also favour 2-D or 3-D effects. This is particularly true for the reactor vessel where 3-D effects are much more pronounced than in system test facilities which have reduced horizontal scales and the scale 1 in the vertical direction.

Then the extrapolation at the reactor scale induces necessarily a loss of accuracy quite difficult to quantify.

1.10 Numerical modelling

Numerical methods in system codes are mostly similar. They use first order finite difference schemes with a staggered mesh and the donor cell principle. The time discretization varies from the semi-implicit scheme used in first versions of RELAP and TRAC to nearly implicit and multi-step schemes used in more recent versions, or the fully
implicit discretization used in CATHARE. These methods are known for their robustness and they are rather diffusive. The CATHARE code takes care of the hyperbolicity of the system in order to warrant stability even for very small time steps and meshes. Theoretically all calculations should be converged in space and time. In practice convergence tests are easily performed for simple analytical tests and some recommendations can be deduced for system tests or reactor calculations.

The problem of convergence in meshing is somewhat different for multidimensional models. As long as there is no turbulent diffusion in these models, convergence tests cannot reach the exact solution. So closure laws must be validated for a given meshing (corresponding to a given numerical diffusion) and possibly with scale 1 experiments. This is particularly important for the downcomer and the upper plenum where the turbulent diffusion may play a role in large break LOCA transients.

2 THE POINT OF VIEW OF THE CODE ASSESSMENT

A more optimistic view of the code capabilities results from the assessment calculations. (see for example Ref 3) Analytical tests are generally well calculated since they were extensively used for the development or the improvement of the closure relations. Separate effect tests where boundary conditions are well known are the only way to determine the accuracy of each closure relation.

Many integral tests are also well calculated. One can consider three main cases:

- All the important phenomena are well predicted with a good timing and a good accuracy.
- The most important phenomena are predicted. The timing and the accuracy are not perfect but it does not prevent from clear conclusions on safety issues.
- Some important phenomena are not predicted or are predicted with a very bad accuracy.

The first type of calculation is still exceptional but the second type becomes more and more frequent.

Possible compensating errors, system effects, and a low density of instrumentation make it difficult to draw final conclusions on physical closure relations from integral tests. However, the analysis of code deficiencies in integral tests calculations is necessary to point out some problems such as:

- Problems of schematizations

The main source of problem is due to a bad choice of schematization. All the codes have several types of modules which have specific capabilities. The user must choose a schematization depending on the transient to calculate. For example the classical schematization of a pressurizer with the CATHARE code uses a simple two node Volume module which is sufficient for all LOCA transients where the pressurizer is rapidly empty. But it is shown in Ref 4 and 5 that for transients such as a Loss of Feedwater with PORV opening or a Multiple SGTR with pressurizer regulations, a more sophisticate module is required.

- Models used out of their domain of validity

The user must care whether the transient exceeds too much the range of validity of some closure relations. It appears that some limitations still exist in the domain of condensation at ECC injection.
Physical process not modelled

In some transients, physical mechanisms can be encountered which were not yet studied in analytical or separate effect tests and which are not modelled in the code. For example, when CCFL occurs in a zone with a complex geometry the corresponding flooding correlation must be established first from analytical tests before it is implemented in the code.

Transients highly sensitive

It is observed that some transients are very sensitive to a certain physical process. In such cases the accuracy of the models should be very good to obtain reliable predictions. The loop seal clearing in some small break transients is an example of very sensitive process.

3 EXAMPLES OF SOME CODE LIMITATIONS

3.1 Interfacial friction in the core

An accurate model for the interfacial friction in the core is of prime importance for a good prediction of the core uncovering situations. The clad temperature excursions will have a space and time extent which depends mainly on it in many small break LOCA transients. Several experiments were devoted to this problem and various correlations were developed (see ref 6 and 7). Despite the important effort made for an accurate prediction there are still assessment calculations which show bad predictions of the core void fraction. These disagreements may be due to some code limitation mentioned in the first paragraph. As pointed out in ref 8 and 6 data suggest possible history effects which cannot be easily modelled. In boiling or flashing flows many small bubbles are created either at the wall or in the bulk. These bubbles coalesce and reach probably a maximum size controlled by their stability. The relaxation time which is associated to this coalescence is probably a function of the number of bubbles created per unit volume for flashing or per unit heating rod surface. According to this the spectrum of bubbles size at a given cross section of the core depends on what occurs upstream and not only on the local vapour superficial velocity. Since the bubble rise velocity mainly depends on its diameter it is not possible to derive a drift velocity correlation or an interfacial friction correlation using only local flow parameters without regarding upstream. If these effects are actually significant one more transport equation should be written for example to predict the dynamics of the interfacial area evolution. In present thermalhydraulic codes where such a model is missing one must accept an uncertainty which is difficult to quantify. The error depends probably on core power, power profile, boiling length, rate of depressurization...

3.2 Stratification prediction

Another example where processes with long relaxation times exist is the establishment of stratified or non stratified flows. The destratification by the Kelvin-Helmholtz instability takes a certain space and time since unstable waves have a finite growth rate. But stratification criteria used in codes assume instantaneous transition. On the other hand, starting from a bubbly mixture stratification will occur in a horizontal pipe if the bubble rise velocity is large enough to dominate the mixing effects of the liquid turbulence (see Ref 7 and 9). But this process of sedimentation requires a certain time which cannot be taken into account in a simple algebraic stratification criterion as described in Ref 7. A transport equation for a quantity representative of the rate of stratification could be necessary particularly for pipes with a small length to diameter ratio.
3.3 CCFL in complex geometries

CCFL and flooding limit are mechanical processes which can be treated correctly by writing a momentum equation for each phase. They contain all the forces playing a role. Buoyancy, interfacial friction, liquid acceleration are the most important, the liquid wall friction being generally small. So provided that the interfacial friction is well correlated two fluid codes should be able to predict CCFL and the flooding limit in any component of a reactor or in a test loop. In practice two fluid codes predict qualitatively the limits but are never accurate enough quantitatively. Academic experiments where liquid in injected through a porous wall in a pipe with a gas flowing upwards is not really a problem. The liquid acceleration effects are minimized and the flow is quasi established. Problem arise when the limitation is located in a more complex geometry, entrance of a pipe, core upper tie plate, hot leg bend... The liquid inertia effects become determinant and are difficult to describe with a 1-D model because of the 3-D nature of the flow. In a circuit flooding is likely to occur first in places where buoyancy has two enemies the gas friction and the liquid inertia and this occurs in singularities which have always complex geometries. 3-D models should then be used but the scale of the phenomenon is generally too small to allow practicable meshing. A solution is adopted in some versions of TRAC and RELAP 5 where the equations are forced - and somewhat violated - to find a prescribed flooding limit correlation given by the user on option for a particular node. A similar approach is in progress for the CATHARE code. It is disappointing that the only way to deal with such an important phenomenon underlines the poor predicting capabilities of advanced codes. This is the best illustration of code limitations associated with singularities.

3.4 Loop seal clearing

The formation of a liquid slug in the intermediate legs of a reactor is a common feature of many small break LOCA transients. This occurs in particular after the rupture of the natural circulation when the reflux condenser mode starts. From this the primary circuit is divided in two parts separated by the liquid slugs in loop seals and in the vessel bottom. These two subsystems are practically uncoupled from the energetic point of view. They follow a thermal equilibrium defined by a saturation temperature resulting from all the energy exchanges. In the hot subsystem - core, hot legs, up side of SG tubes - the temperature level is generally controlled by the secondary side except if a break in this part is able to discharge more vapour than the core produces. In the cold subsystem the energy balance generally depends on the presence of a cold leg break and of safety injections, both of them giving a trend to a temperature decrease. Most of the time a depression of the cold part is induced which shifts water from core to downcomer. This dangerous situation stops when the liquid level in the descending part of the intermediate leg reaches the horizontal part. The loop seal clearing occurs either simultaneously or at different times in the different loops. When the process is slow it is very sensitive to small asymmetries between the loops. Once it happens in one loop a stable state may be reached preventing from its occurrence in the other loops. Codes have many difficulties to predict these non symmetrical behaviour correctly. It is a typical problem of predictability as is encountered by meteorologists who knows that in nonlinear systems small causes may sometimes have big effects.

3.5 Gravity driven reflooding

The reflooding process has been extensively studied and many analytical tests are available to assess the basic models involved in the process. Many effects are to be taken into account and the main difficulties are related to the following aspects:
Small scale two dimensional effects located at the quench front condition the whole process. Classical correlations for heat transfers and mechanical interactions are no more valid in this non established flow.

The droplet flow in the region downstream the quench front requires a very accurate physical modelling. The droplet size controls the heat transfers and consequently the peak clad temperature. The drop size spectrum evolution depends on several mechanisms: film splitting at the quench front, vaporization, break up by vapor turbulence, break up by dry spacer grids, capture by rewetted spacer grids. All these effects are only approximated in a 6-equation model where a simple local mean drop diameter correlation can be used.

Despite these difficulties, assessment calculations of reflooding tests with a constant flowrate are often satisfactory. Some more problems are encountered when calculating reflooding in system tests (LOFT, PKL, BETSHY). At the beginning of the transient, when water enters the core, oscillations are initiated which may be amplified if a lot of water is carried over and vaporizes on hot walls in the upper plenum, in hot legs or in steam generators. If the amplitude is high a large amount of water can be lost at the break and the core reflooding be strongly affected. The example of the CATHARE code calculating such situations showed (Ref 11) that a probable overestimation of the interfacial friction just downstream the quench front could excite the oscillations a long time when they are rapidly damped in the experiment. The core thermalhydraulic modelling developed and assessed in smooth transients does not seem fully relevant in more unsteady situations.

System effects are also difficult to control: the water entrained out of the core is partly deentrained in the upper plenum and partly sent to the hot legs. The repartition determines the level of the steam binding effect which is an important limitation for the core heat removal. This repartition is probably dependent on the drop size spectrum and on the geometry of the internal structures in the upper plenum. 3-D codes as well as 1-D codes may have difficulties due to the description of a single liquid field.

3.6 Condensation at ECC injection

The direct contact condensation at ECC injection is also a source of mispredictions in several transients. The most difficult situation is related to accumulator discharge in large break LOCAs where a violent condensation takes place and may induce flow instabilities. The formation of liquid slugs in cold legs was observed in system tests at different scales (LOFT up to scale 1 (UPTF)). A strong condensation is necessary to create the slug, then its evolution depends on the thermal diffusion at the slug front. This situation is very difficult for the 1-D models which are rather adapted to smooth axial variations. Void fraction fronts are known to induce problems of discretization (such as the water packing problem). Moreover the axial heat diffusion which governs the condensation and consequently the slug movements is not modelled as anyway it would require a very fine meshing.

In small break LOCAs the injected flowrates are lower and the condensation is less unstable. It is shown in Ref 12 and 13 that a good modelling of the condensation should take into account all the sources of liquid turbulence. This is relatively easy for established separated flows where turbulence is created by wall shear and interfacial shear. It becomes more difficult when turbulence is generated in singularities such as the injection zone or the downcomer entry nozzle. According to some experiments (see Ref 11) most of the condensation is located in these singularities, particularly in the injection zone. This makes the process very dependent on local geometrical parameters and does not allow generalizations.
CONCLUSIONS

Best estimate thermal-hydraulic codes are irreplaceable tools for PWR safety analysis. They have already proven their capabilities to predict at least qualitatively and sometimes quantitatively many basic features of the accidental transients. In order to make the best use of such complex tools, a good knowledge of their limitations is necessary. Although important progress have been made up to now, some difficulties remain which seem difficult to overcome:

- Flow perturbations in geometrical singularities have sometimes an important effect on a transient (CCFL, condensation enhancement, grid effects in reflooding,...) Small scale 3-D effects cannot be described by present 3-D models. So they must be studied experimentally and give specific correlations.
- Present 2-fluid models are limited when physical processes masked by the averaging have long relaxation times (stratification, bubble coalescence) which would require one more differential equation.
- Flow situations which are typically 3-dimensionnal with important turbulent mixing effects cannot be treated correctly by present 3-D models.

A list of recommendations for the code developers and for the code users can be deduced from this examination.
- Code developers should make big efforts for providing precise User Guidelines They should also list all the presumed limitations as this paper tries to do.
- The main efforts should concern the improvements of the 3-D models and the modelling of flow perturbations in singularities.
- The user must be conscious of the code capabilities and limitations. A thermalhydraulic code cannot be used like a black box.
- A critical analysis of each calculation should concern the following points:
  - Does the calculated transient remain within the validated domain and within the range of two phase flow situations already investigated and modelled?
  - Is'nt there a very high sensitivity of the transient to some parameters or physical processes?

If positive answers can be given to both questions, the uncertainty analysis becomes easy. But in the other case, all uncertainty evaluations remain questionable.

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TWO-PHASE FLOW INSTRUMENTATION

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ABSTRACT

A careful measurement of the relevant two-phase flow parameters is the basis for the understanding of many thermohydraulic processes. Especially in the nuclear safety research where accident scenarios have to be simulated in experimental setups and predicted by complex computer code systems a reliable two-phase instrumentation is substantial for the connection between analysis and experiment. Ambitious development programs have been carried out in many institutions and countries to promote two-phase instrumentation. Advantages as well as limitations of some of these systems will be discussed in the paper. In the last 10 - 15 years good progress has been made. However there are still goals for further developments and there is still the fact that in many cases - measured data taken from large experimental facilities cannot be compared directly to the parameters calculated by the codes. Careful comparison and interpretation of both calculated and measured results by experienced researchers will be the key for thermohydraulic understanding of complex two-phase phenomena also in the future.

1. INTRODUCTION

The measurement of two-phase flow quantities is substantial for the understanding of many technical processes esp. reactor system behavior under accident conditions and is a prerequisite for proper code modeling and verification.

More than 25 years have been spent on developing various solutions for measuring two-phase flow with the aim to
- get local or integral information,
- built very sensitive (but mainly also fragile) instruments or try to get rather valid information with rigid sensors as well and
- apply techniques simple to use and to interpret or install highly sophisticated instruments such as laser techniques or \( \mu \)-waves.

It turned out that there is no and perhaps never will be a Standard or Optimum Instrumentation. Measuring two-phase flow will always need experienced researchers using special solutions for each required purpose.

Successful application of a measuring system in a two-phase test setup does not automatically mean its qualification for nuclear reactor conditions or even for other loops if environmental conditions change such as radiation levels or even only water quality.

On the other hand two-phase measuring techniques in many cases do not measure directly the two-phase properties needed to verify the two-phase models.
such as local shear, velocities of the single phases etc. so that indirect comparison of calculated and measured data is needed.

Despite of these not very encouraging facts the large reactor safety research programs performed in the last decade as well as the detailed development work carried out at numerous universities and research institutions have increased our knowledge in two-phase flow measurement techniques significantly.

Fundamental work done e.g. at Harwell or in Germany at the universities of Munich and Hannover lead to helpful decision trees so that new users must not start again with trial and error methods and waste a lot of time in selecting proper instruments for normal applications.

But the key to fundamental understanding of two-phase flow is still a careful development especially for special and complex geometrical applications such as the end box instrumentation of UPTF at the reactor core to upper plenum interface, an even more careful testing and calibration of the instruments if possible in situ or in an reproduced environment and geometry and a development of special algorithms if necessary to interpret later all measuring signals under all possible two-phase conditions.

The paper will discuss recent experience with several two-phase flow instruments used in the reactor safety research programs such as 2D/3D/UPTF, PKL, BETHSY, ROSA including also the experience obtained with liquid level detectors developed to measure RPV inventory transients under reactor accident conditions in PWRs or BWRs.

Current needs for further efforts will also be discussed.

2. REQUIREMENTS IN REACTOR SAFETY RESEARCH

Reactor safety investigations deal with most unlikely and fortunately very unusual accidents or incidents in nuclear power plants. Reliable thermohydraulic codes are therefore needed to predict the transient behavior of the power plant under anticipated accident scenarios to

- prove the effectiveness of safety systems
- give advises for possible improvements of hardware or procedures
- estimate the possible consequences.

The development and assessment of these codes is based on proper physical modeling of all relevant thermohydraulic phenomena.

So at first a wide range of two-phase flow investigations is oriented to so called "separate effect tests" concentrated to single parameters. In many of these cases two-phase flow phenomena can be simulated in tests under clear boundary conditions, so that also the instrumentation used can be calibrated properly in advance.

In these separate effects experiments many kinds of two-phase measurements devices have been used with good success as the instrumentation could be optimized to a few parameters only. Here also methods such as laser-doppler, ultrasonic-doppler, hot wire were applied to measure phasic velocity. Also very sophisticated methods like x-ray computed tomography has been used to measure cross-sectional distribution of phases or even void distribution in bundles.

Two phase flow instrumentation is also necessary for the interpretation of "system effects tests" e.g. simulating the thermohydraulics of a PWR in a scaled-down
test facility. In this case the instrumentation shall prove the quality of code predictions and identify uncertainties still existing.

Under these conditions instrumentation consisting of mechanical parts installed in the flow path can be of influence on the system behavior itself. So an optimum between the amount of measuring information needed and flow disturbance must be found.

In system effect tests very often signals are also taken from simple single-phase measurements and used for mass and energy balance methods which give a good check for the overall code performance as well.

After TMI accident the small break accidents and transients were discussed in much more detail. As a result of the worldwide discussions the need of a reliable liquid level measurement in the RPV of a PWR was elaborated. The recent discussions of AM-procedures to prevent core melt scenarios - even under transients like station blackout - increase the need of such a measurement method which must function under all possible transients and two-phase conditions.

Under reactor operation conditions much different requirements occur to prove reliable instruments such as radiation level, boric acid, flow induced vibration, etc. Caused by this a very conservative way of selecting instruments is the case.

3. EXPERIENCE WITH TWO-PHASE INSTRUMENTATION

In the following the discussion is concentrated to experience obtained in system effects test facilities (the paper cannot cover the wide range of all measuring methods applied to numerous separate tests; see 2.). The authors, all engaged in German reactor safety programs hope that thanks to international cooperation and information exchange the remarks given below are valid also for the other research programs. However some new developments may not be known or experienced yet and may be subject for discussion after the presentation.

3.1 PIPE FLOW MEASUREMENTS

Mass flow is a difficult parameter to measure in two-phase flow as the phases have different densities and velocities. At present no attempts are being undertaken to develop a handy metering system on the basis of a single measuring principal. On the contrary solutions offered to date seek to combine various techniques. The approaches are mostly based on measuring techniques that have already been proven in single-phase flow during industrial applications.

The number of techniques which have to be combined depends on the number of phases/components (if multi-phase flow), their distribution in the flow and the accuracy requirements.

The greatest impetus for the recent research and development effort concerning the measurement of total mass flow rate and composition of a multi-phase/multi-component flow in pipes probably comes from the oil production industry, where there is a need to measure accurately the production of oil and gas mixtures produced by individual wells. The conditions under which the meters are required to operate vary over a wide range regarding flow rates, mixture composition, pressure and temperature. In addition to coping with these conditions, metering also has to be applicable to different flow patterns.

A wide-ranging assessment of multi-phase/multi-component mass flow metering systems has been completed by Hewitt /1/2/ and /3/. The studies examine the ap-
plicability of different possible techniques capable of providing total mass flow and compositional measurements of an oil/water/gas mixture.

As a first step the potential methods for mass flow, velocity, pressure drop and density measurement feasible in the area of oil field operation have to be identified, see table 1. In this context installability and maintainability were examined, as well as radiation safety and power source requirements, and the suitability of the instrument for licensing by the regulatory authorities. Other criteria included lifetime expectancy, signal quality, cleaning capability, interference with other instruments, and availability. By examining the various techniques in the light of the criteria identified above, a short list of possible techniques was produced by the construction of a logic diagram, giving rise to a number of combinations of metering techniques (fig 1).

A combination proposed by Siemens/KWU in 1988, /4/, consists for example of venturi tube, non intrusive impedance probe and single-beam gamma-densitometer, fig. 2, and corresponds to the results of the study made by Hewitt. The measurement principle is based on the idea that the venturi tube with its reduction in cross-section and associated flow acceleration homogenizes the multiphase/multi-component flow. Capacitor and gamma-densitometer, which are used for compositional measurements, are therefore installed in the throat section of the venturi tube to reduce the extent to which their signals are affected by the flow patterns that occurs with this technique. The total flow rate is determined by means of differential pressure measurement at the venturi tube. Testing in a rig with oil, water and air under atmospheric conditions showed that the metering system functions properly and supplies easily evaluable signals only if the various components are distributed virtually homogeneously throughout the flow and with oil or air forming a continuous phase. Under these conditions, calculated partial mass flows of air, oil and water can be accurate within ± 25%.

Siemens/KWU also tested a device similar to the described one which combines a venturi meter and an intrusive capacitor (plate type). This combination has been used in water/steam mixtures up to about 230 bar and 400°C (horizontal orientation, steady-state conditions). The performance of the meter for steam mass fractions between 0.01 to 0.9 was within ± 20 % (fig. 2a).

In reactor safety studies such as PKL (Germany) CCTF, SCTF and ROSA (Japan), LOBI (EC), BETHSY (France) other combinations have been used, with the aim to minimize the flow disturbance as homogenizing was not likely /5/ to /6/.

The PKL and CCTF devices located in the horizontal hot legs are combinations of a 3-beam gamma-densitometer, a full flow turbine and a drag body meter /9/. The instruments have been calibrated carefully in a two-phase flow loop.

If phase velocities are assumed to be equal (slip s = 1) in the calculation for total mass flow only the combination of two informations is required (e.g. gamma-D + drag or gamma-D + turbine or turbine + drag). In this case a comparison of the different results for accuracy estimations is possible. In most of the cases different phase velocities must be assumed so that all three instrument signals are necessary to determine mass flow rate.

The instrument signals and the evaluated mass flow for a typical hot leg oscillation slug flow situation simulating a large break LOCA is shown in fig. 3.

The results are reasonably good. More trouble with this instrumentation is obtained under low flow and esp. under counter current flow conditions (reflux condensation).

The type of instrumented spool piece used in the BETHSY facility (France) is specially designed to measure also stratified flow conditions (fig. 3a). The combina-
tion of a 3-beam Gamma-densitometer and 3 turbopropes ($\varnothing$ 12 mm) distributed along a vertical diameter of the horizontal pipings (in hot and cold legs) provides mass flow rate measurements in stratified flow with a maximum uncertainty of ±15% as long as the liquid level remains above the lowest turbine.

In the ROSA-Large Scale Test Facility also 3-beam Gamma-densitometers are installed with the aim to measure flow distribution; flow rates are measured at the steam generators exit in single phase flow.

**UPTF Pipe Flowmeter**

In large pipes like in the 1:1 simulation of UPTF/10/10a/ full flow instrumentation is not applicable. In the UPTF five Pipe Flow Meters (PFM) were installed in the four hot legs and in the broken cold leg. Each of the PFM use an array of four drag disks and three gamma-densitometer beams along with an absolute pressure and temperature measurement (fig. 4). The signals from all instruments have to be interpreted to determine flow regime, void fraction, phase velocities, phasic densities and total mass flow. UPTF pipe flowmeter data interpretation was based on algorithms adapted from other facility applications [10b], supplemented by limited in-situ, single-phase calibration at UPTF (fig. 5).

To get an idea about the quality of the UPTF test results, test calculations were done to compare "hand-calculated" values with results, calculated by the PFM algorithm. These calculations show, that the algorithm is able to determine homogeneous and stratified flow in most cases. For test calculations with annular flow the algorithm sometimes determines stratified or tilted stratified flow because of the local positioning of the probes. However, in such cases the calculated mass flow was in a good agreement with the hand-calculated input data.

High discrepancies occurred for situations when tilted stratified flow was determined by the algorithm in the case that the assumed phase boundary was on the line between two drag paddles. Also for two-phase flow with big differences between the phase velocities, higher differences between both calculated values were obtained.

For some tests, the total mass flow rates calculated by the PFM algorithm were compared with single-phase mass balance calculations. The deviation of the total mass flow was between 6 and 10%. This is a good result for a two-phase flow measurement.

**3.2 UPTF END BOX INSTRUMENTATION**

A special measurement approach was developed in order to determine the mass flow of each phase (steam and water) through the upper end-boxes (193 Fuel assemblies, FA), the interphase between reactor core and upper plenum. The task was to determine the steam and water mass flow for the following flow regimes:

- Co-current downflow
- Water downflow
- Counter-current flow
- Co-current upflow with saturated or subcooled water in the upper plenum.

The simulated transients included the endphase of blowdown (EOB) as well as the refill and reflood phase of a large break LOCA in a PWR with cold leg or combined (cold and hot leg) ECC-injection.

At least two different flow measurement signals are required for determination of the total mass flow in a two-phase flow. The flow restricting area of a fuel as-
semsly end-box is the tie plate which is about 20 mm thick and perforated with a lot of holes of 12 mm diameter. The tie plate is predestinate for the measurement location, since the three-dimensional flow around the end-box is forced to flow vertically in the tie plate holes associated with a vertical force on the tie plate. Therefore, the tie plate force was chosen as one of the measurement parameters. For this purpose, a cutout section of about 60 % of the whole tie plate area was used as a drag-body, where all transducers for the force measurement were accommodated within structural members of the endbox, so that this instrument sampled a large amount of the flow with minimum disturbance to the flow.

A turbine, mounted in the soak flow above one tie plate hole, was chosen to measure the second required flow parameter, since a local fluid density measurement by gamma-ray device was not possible. Information about the local water level above the tie plate was gained from a purged ΔP-measurement (fig. 6).

Extensive calibration tests were performed for this combined measurement equipment of tie plate drag-body and turbine (called flow module) in an one-fuel assembly test facility covering the above mentioned flow regimes and a pressure range of 2.5 to 6 bar. The calibration in an endbox mock-up consisted of 345 sets of data points and took great efforts in man power and expenses (fig. 7).

A special flow module algorithm had to be developed to determine the mass flow of each phase, since standard evaluation methods deliver only the total two-phase mass flow. The relationship between the steam/water mass flow through the tie plate and the drag-body force was gained from the momentum equation applied to an appropriate control volume around the tie plate, while the correlation for the turbine rotation was derived from a torque balance of driving and braking forces acting on the rotor blades. Based on these correlations and the flow module calibration test results, a computer code was developed which determines the steam and water mass flow rates from measured drag-body force and measured turbine rotation as follows (Fig. 8):

The calculation starts with determining a steam flow rate from measured drag-body force assuming no tie plate water flow (steam only). For the tie plate flow investigated on Fig. 8 this steam only flow would result in a much higher turbine rotation than measured. Therefore, the assumed steam flow is successively decreased and the water flow increased until the true steam/water flow rate is found which delivers identical values for the calculated and measured turbine rotation. The standard deviation for this tie plate mass flow measurement is listed in Table 2.

3.3 LOCAL PROBES IN TWO PHASE FLOW

For void fraction or even only detection of the presence of water (Yes/No) various types of local probes are proposed /11/ to /13/. The most rigid ones are impedance type probes measuring either capacitance or conductivity. A more promising development seems to be optical probes as e.g. installed in the UPTF facility.

Liquid Level Detectors (LLD) and Fluid Distribution Grids (FDG) in UPTF:

In the UPTF 705 optical FDG's and LLD's were installed primarily in the upper plenum and the downcomer area, to detect the presence or absence of water at single locations, using the reflection/refraction of light at a sapphire/fluid interface. All sensors were calibrated in-situ at known wet and dry conditions. These values than were used to set the wet/dry bi-stable discrimination by the sensor electronics.
The results of the UPTF FDG/LLD sensors can be used as an additional information about steam/water distribution in the case of separated steam and water flow. Fig. 9 shows a comparison of temperature and FDG/LLD readings for such a situation in the UPTF downcomer. Both measurements are in a good agreement.

For homogeneous two-phase flow the FDG/LLD results should be carefully interpreted. Fig. 10 shows the same comparison for steam/water mixture flow through the downcomer during the end of blowdown phase. At that time, the results are completely different. During the whole EOB phase, all FDG sensors show only "wet". Due to dispersed droplet flow the probe tips seem to be wet enough all the times.

A comparison of LLD signals with collapsed water level measurements can not confirm these LLD results. Only at the end of the test, when the water and steam phases are well separated, the LLD's are in an acceptable agreement again with the measured collapsed water level. The time before, the LLD signals indicate a water level much more higher, than measured with the DP-cell.

Conductivity Liquid Level Detectors in PKL

LOFT-type LLDs were installed in the core region inside of unheated rods and in the upper plenum.

As the probe tip was oriented downwards the installation of splash shields were almost of no use. In case of dispersed flow or even more in case of bubbly flow (water continuous phase) the sensors tend to overpredict water inventory as to be seen in fig. 11 (comparison with collapsed level measurements).

Impedance Probes in PKL

Other local measurements for density are the so called Impedance Probes (tested in PKL for the 2D/3D projects; developed by ORNL) which consist of two pin or flag or multi string type sensors (fig. 12) assembled within a ceramic insulator and mounted on a support structure e.g. unheated rods in the core or reactor internals in the upper plenum or on a separate holding device. The void fraction is obtained by analyzing magnitude and phase of the sensors impedance. Calibration tests showed encouraging result, however in flooding experiments with high water entrainment a liquid film deposits on the surface of all unheated structures so that water bridging effects on the insulators often caused large differences in the signal levels, the absolute values of void fraction did not correspond anymore to the main part of the sensors cross section (fig. 13).

Local Velocity or Flow Direction Detection

Additional help for the comparison between experiments and calculations can be taken from local measurement for flow direction and velocities.

Of special interest during flooding experiments was the core to upper plenum interface.

In the UPTF facility so called Break Through Detectors are used which are drag-body type sensors just below 3 holes of the upper tie plate of a full element (just below the Flow Modules). As these instruments were rather inexpensive 94 of these sensors are installed (fig. 14).

The sensors could identify the downflow of water for large quantities (hot side injection) as well as for small quantities (deentrainment in the upper plenum in case of cold leg ECC injection).
In the PKL facility some local miniturbines installed on the top of a single hole of the tie plate have been used during flooding experiments. Fig. 15 shows a comparison between measured turbine velocity and a qualitative interpretation (subcooled, saturated or superheated) of local fluid temperature measurements. It can be seen that in case of a longer indication of down flow the fluid temperature was always subcooled indicating a solid water breakthrough.

Flow Visualisation

Large facility dimensions and relatively low system pressures such as in UPTF encourage experimentalists to try to visualize the flow as a good help to interpret other sensor signals nearby. Various attempts have been undertaken in UPTF and other two-phase test facilities taking photos (dubble flash), video or high speed recordings through windows or use lens systems or miniature cameras to enter into the flow channel. Especially the JAERI video probe/16/ should be mentioned which is small in size (12 mm diameter or even smaller 2.8 mm diameter for lower pressure 12 MPa), it can withstand 20 MPa and over 370°C. The small probes could identify not only flow pattern but also small voids while Δp-methods are not sensitive enough.

As long as the continuous phase is steam with smaller water droplets (dispersed flow) the flow visualization is rather successful. In this flow regime also laser techniques for droplet size and velocity or flow direction measurements (3D) can be applied. These systems are also useful in the late phase of a flooding process or at the transition from single phase to two phase flow when the system is almost full of water and only a few steam bubbles occur. However a very interesting part e.g. of a flooding process takes place when slug flow or churned flow occurs. During this period of time flow observation is reduced to just a few mm next to the glass or sapphire windows as the two-phase mixture becomes non transparent. Then the support in interpretation of other two phase sensor signal located nearby is very poor.

3.4 MASS AND ENERGY BALANCE METHODS

As indicated previously in many cases it is not possible to measure the two-phase flow parameters that are easy to compare with thermohydraulic models. Often only an integral comparison is possible which allows to assess the code capability. In these cases it is necessary to make comparisons with rather simple and reliable instrumentation such as Δp-measurements.

One example is the vertical void fraction distribution in a heated core derived from a fine net of Δp-sensors. Assuming that acceleration and friction pressure losses can be neglected the Δp readings between the pressure taps can be interpreted as collapsed liquid level or mean density. Fig. 16 gives an example how the relation between total collapsed level and swell or two-phase mixture level can be derived.

An other example of UPTF is the interpretation of the entrainment behavior during a bottom flooding experiment using Δp-measurements and the method of water separation. Fig 17 shows entrainment scenarios for different points of time in the transient.

All experimentalists try to use these kind of back-up measurements to check the two-phase instrumentation installed and to make sure that higher errors do not mislead the interpretation during a long transient.
3.5 LIQUID LEVEL MEASUREMENT IN THE PRESSURE VESSEL OF A PWR

The water level of the pressurizer is used to monitor the water inventory of the primary system of a PWR. The TMI accident however demonstrated that in case of a pressurizer leak (valve stuck open etc.) this is not a reliable information for reactor pressure vessel inventory.

To fulfill the requirements of the licensing authorities a in-vessel probe was developed which is rugged, reliable and consists of proven parts. Based on the special requirements of a nuclear system (radiation level, boric acid (lower pH value) high velocities and flow induced vibration during normal operation, no pressure boundary penetration below the reactor coolant lines, no purge system operational also under accident conditions) practically none of the many two-phase instruments used in the laboratories were applicable.

For a point-wise detection of the liquid level in the upper plenum a probe was selected consisting of two resistance thermometers (one wrapped with a heating element). Being inserted into water heat transfer is so high that the heat input from the heating element does not influence the temperature of this sensor. Being in steam environment due to heating one sensor gets a higher temperature than the other one. This difference can be measured by a wheatstone bridge. The sensors are shielded by a tube with only small openings at the bottom and top end so that besides very rapid depressurization situations (where the probe signal is not relevant) the sensors measure always separated collapsed liquid level conditions for all flow regimes occurring in the upper plenum (fig. 18, 19).

Prototypes have been tested under typical small break LOCA situations. One probe was also used in many PKL experiments and showed reliable signals. In the meantime 3 stalks with 3 sensors each were installed in most of the German PWRs /17/.

As accident management procedures (in case of beyond design basis accidents) such as bleed and feed measures are currently investigated to a great extend, the use of these sensors have been discussed and examined in detail also under these conditions. It turned out that also under very high system pressure the probes provide reliable signals.

At present the development of a redundant, diversified system is under discussion with different options such as

- stalk-type sensors in the upper plenum with continuous (eg. Δp) measurements
- ultrasonic measurements at the hot leg using a proven technology installed already in some BWR plants /18/.

4. CONCLUSIONS

Good progress has been made in measuring two-phase flow mainly influenced by the large reactor safety research programs and by the instrumentation development for the oil industry. The authors tried to give some examples and comments to experience especially obtained in the German reactor safety research. These examples indicate that reliable instrumentation is still limited and often cannot measure the physical properties or have to be installed at locations other than required and specified by the code developers. The cooperation between the experimentalists and the analysts will further rely on feasible solutions and compromises.
For instrument developers there are still ambitious targets for future work such as:
- simple to use low flow measuring techniques (<1 cm/s)
- high void fraction local density measurements e.g. for direct measurement of water entrainment
- on-line concentration measurements for
  • non-condensibles in steam atmosphere
  • additives e.g. boric acid in the water phase
- automated ultrasonic interpretation for film thickness measurement
- video image evaluation into velocity informations etc.

The authors also thank the teams responsible for reactor safety research experiments in other countries for comments and advises to this paper.

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Table 1: Potential measuring devices for multi-phase flow according to /1/

Table 2: Standard deviation for UPTF tie plate mass flow measurement

Fig. 1: Selection chart for metering combinations according to Hewitt /1/

Fig. 2: Circuit diagram of the metering system

Fig. 2a: Measurement accuracy related to the total and partial flow rates of a water/steam mixture

Fig. 3: Arrangement of measuring devices within the "instrumented spool pieces" and some measurement results

Fig. 3a: BETHSY (CENG) hot leg spool piece

Fig. 4: UPTF Pipe Flow Meter - Arrangement of Instruments

Fig. 5: Evaluation of two-phase mass flow in the primary loops

Fig. 6: Arrangement of Instruments in the tie plate area

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Fig. 8: Schematic diagram for determining steam and water mass flow rates from flow modul measurement signals

Fig. 9: Water level formation in downcomer
Comparison of optical LLD with fluid temperatures

Fig. 10: Water level formation in downcomer
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Fig. 11: Detection of water distribution in a large volume using a conductivity liquid level detector system

Fig. 12: Different types of impedance probes for local velocity and void fraction measurement

Fig. 13: Comparison of void fractions determined with impedance probes and geodetic head (p) measurements

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Fig. 18: Siemens/KWU RPV-Level probe
Thimble and sensor arrangements

Fig. 19: Siemens/KWU RPV-Level probe
Water level versus time during small break LOCA
Weighing of a pipe
Vibrating tube density meter
Acoustic attenuation
Impedance
Single-beam gamma-densitometer
Broad-beam gamma-densitometer
Multi-beam gamma-densitometer
Gamma-ray scattering
Neutron attenuation
Neutron scattering
Microwave attenuation
In-the-line (grab) sampling
Isokinetic sampling
Turbine meter
Vortex shedding meter
Acoustic velocity (pulse and return)
Acoustic cross-correlation
Electromagnetic flow meter
Pulsed photon activation
Pulsed neutron activation
Radioactive tracer method
Drag disk or screen
Variable area orifice
Orifice meter
Venturi meter
Pitot tube
Tube pressure drop
Pressure fluctuation signature
True mass flowmeter
Coriolis meter
Neutron interrogation
Multi-energy gamma-densitometer

Table 1: Potential measuring devices for multi-phase flow according to /1/
<table>
<thead>
<tr>
<th>Condition</th>
<th>Variable</th>
<th>Standard Deviation</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Low upflow and countercurrent flow (CCF), saturated conditions</strong></td>
<td>Tie plate steam mass flow</td>
<td>± 10 %</td>
</tr>
<tr>
<td></td>
<td>Tie plate water mass flow</td>
<td>± 52 %</td>
</tr>
<tr>
<td></td>
<td>Tie plate total mass flow (steam and water)</td>
<td>± 32 %</td>
</tr>
<tr>
<td><strong>High upflow (total mass flow &gt; 3 kg/s/FA), saturated conditions</strong></td>
<td>Tie plate steam mass flow</td>
<td>± 11 %</td>
</tr>
<tr>
<td></td>
<td>Tie plate water mass flow</td>
<td>± 28 %</td>
</tr>
<tr>
<td></td>
<td>Tie plate total mass flow (steam and water)</td>
<td>± 12 %</td>
</tr>
<tr>
<td><strong>Two-phase upflow with subcooled water above tie plate</strong></td>
<td>Tie plate steam mass flow</td>
<td>± 21 %</td>
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<tr>
<td></td>
<td>Tie plate water mass flow</td>
<td>± 36 %</td>
</tr>
<tr>
<td></td>
<td>Tie plate total mass flow (steam and water)</td>
<td>± 28 %</td>
</tr>
<tr>
<td><strong>Steam only upflow with subcooled water above tie plate</strong></td>
<td>Tie plate steam mass flow</td>
<td>± 15 %</td>
</tr>
<tr>
<td></td>
<td>Tie plate total mass flow (steam and water)</td>
<td>± 72 %</td>
</tr>
<tr>
<td><strong>Cocurrent downflow</strong></td>
<td>Tie plate steam mass flow</td>
<td>± 19 %</td>
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<td></td>
<td>Tie plate water mass flow</td>
<td>± 23 %</td>
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<tr>
<td></td>
<td>Tie plate total mass flow (steam and water)</td>
<td>± 17 %</td>
</tr>
<tr>
<td><strong>Water downflow</strong></td>
<td></td>
<td>± 9 %</td>
</tr>
</tbody>
</table>

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Y - Source
Cs 137
3.7 × 10^11 Bq

Sodium-iodide detectors

Drag rake

Absolute pressure transducer

750

20°
Algorithm developed by EG & G

Developed at LOFT SEMISCALE

Flow pattern  Fluid force  Void fraction  Total mass flow  Average fluid velocity

Gamma density measuring system  Drag rake  Absolute pressure transducer  Thermocouple  Temperature

Fig. 5: Evaluation of two-phase mass flow in the primary loops
Fig. 6: Arrangement of instruments in the tie plate area
Algorithm developed by Siemens/KWU

Total mass flow
Water mass flow
Steam mass flow
Flow direction

Velocity
Fluid force
Water level
Temperature

345 calibration tests with one bundle

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String probe

Incore prong probe

Incore flag probe

Upper plenum flag probe
Fig. 13: Comparison of void fractions determined with impedance probes and geodetic head (Δp) measurements
94 detectors installed 7 mm below tie plate

4 systems failed just after start of test phase; since that time the systems are working without problems

Fig. 14: Breakthrough detector
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Fig. 19: Siemens/KWU RPV-LEVEL probe Water level versus time during small break LOCA
Numerics of Codes: Stability, Diffusion, and Convergence

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ABSTRACT

The numerical methods used in the primary US reactor safety codes are summarized. The basic Courant-type stability limits for these codes are reviewed, and more subtle stability problems arising from the explicit evaluation of various friction and heat-transfer coefficients are discussed. Much of the stability and robustness of these codes has come at the expense of high numerical diffusion. The impact of numerical diffusion is illustrated. The question of convergence of solutions of the difference equations to those of the original differential equations is also addressed.

1. INTRODUCTION

Although the contents of this paper are applicable in varying degrees to a wide range of codes, specific remarks are limited to versions of the TRAC, RELAP, and (to a limited extent) the RETRAN code series. The stability properties of these codes, resulting from the choice of time level for pressure gradients and terms in the mass, energy, and momentum fluxes, have been well studied. Linearized stability analysis of the full set of two-phase flow equations provides only limited information, due to the complexity of the resulting algebraic equations. However, much has been learned from linearized analysis of the basic numerical methods applied to single phase flow, and detailed numerical trials of the full two-phase codes. Unfortunately, a large class of stability problems, resulting from the evaluation of various heat transfer and friction coefficients (wall and interfacial) at the old time level, has been inadequately addressed. Since these instabilities are generally manifested as bounded oscillations, it has been argued that they do not effect the mean predictions of system behavior. One example is presented of significant error in mean behavior caused by such an instability.

The questions of diffusion and convergence have not been as widely studied as stability. Since the inception of most reactor safety codes, it has been recognized that the chosen spatial and temporal difference methods introduce substantial numerical diffusion. However, for the vast majority of problems of interest this has not been a significant problem. Attempts to introduce less diffusive numerical schemes degraded the robustness of the methods to levels unacceptable in production codes. The recent importance of core oscillations in BWR's has resulted in renewed interest in the effects of this numerical diffusion and attempts to introduce improved methods.

The formal convergence of the difference equations used in reactor safety codes has generally been acceptable. However, in a less formal sense convergence problems can result from the use of a form
of the energy equation that is not fully conservative. For flow through an abrupt area change, the
temperature change downstream will be predicted incorrectly regardless of the length selected for the
mesh cells. This problem has been recognized for many years, but most recently became an issue as
a result of an attempt to use RELAP to model the behavior of a reactor containment.

2. Basic Equations

Because of my background with the TRAC-PWR program, I will begin by discussing the numerical
methods used in that code series, and then discuss key points of similarity and difference for other
safety codes. To demonstrate these methods, only a simplified model for one-dimensional, single-phase
flow in a pipe will be considered. References are provided for the full two-phase models and
associated difference equations. The differential equations for this simple model are:

\[
\frac{\partial p}{\partial t} + \nabla \cdot (pV) = 0,
\]

(1)

\[
\frac{\partial pe}{\partial t} + \nabla \cdot (peV) = -p \nabla \cdot V,
\]

(2)

and

\[
\frac{\partial V}{\partial t} + V \cdot \nabla V = -\frac{1}{\rho} \nabla p - K V |V|.
\]

(3)

Here, K is a wall friction coefficient that may be a function of velocity and fluid properties.

TRAC-PWR codes beginning with TRAC-PF1 [1] employ the stability-enhancing two-step (SETS)
method[2,3] to solve the flow equations. This is an extension to the standard semi-implicit methods
found in earlier versions of TRAC [4] and versions of RELAP through RELAP5/MOD2 [5]. SETS
has the advantage that it eliminates the material Courant stability limit of a semi-implicit method, and
the computer time per cell per time-step is reduced by at least a factor of 5 over that of a fully
implicit method.

A staggered spatial mesh is used for the finite-volume equations, with thermodynamic properties
evaluated at the cell centers and the velocity evaluated at the cell edges. Only difference equations
on the one-dimensional version of this mesh will be demonstrated, but the generalization to two- and
three-dimensional versions is not difficult. To ensure stability and to maintain consistency with
differencing in previous TRAC versions, flux terms at cell edges use donor cell averages of the form
\( (\nabla \cdot (\nabla))_{j,ua} = V_j V_{j,ua} \cdot V_{j,ua} \geq 0 \)
\[ (\nabla_j \cdot \nabla)_{j,ua} = V_j V_{j,ua} \cdot V_{j,ua} < 0 . \]  

Here \( Y \) may be any state variable. Other forms of this average may maintain stability with higher order spatial accuracy but they have not been carefully studied. With this notation the one-dimensional finite-difference divergence operator is

\[ \nabla_j \cdot (\nabla) = (A_{j,ua} (\nabla)_{j,ua} - A_{j,ua} (\nabla)_{j,ua}) / \text{vol}_j , \]  

where \( A \) is the area of the cell edge and \( \text{vol}_j \) the cell volume. The term \( V \nabla V \) becomes

\[ V_{j,ua} \nabla_{j,ua} V = V_{j,ua} (V_{j,ua} - V_{j,ua}) / \Delta x_{j,ua} \cdot V_{j,ua} \geq 0 \]
\[ V_{j,ua} (V_{j,ua} - V_{j,ua}) / \Delta x_{j,ua} \cdot V_{j,ua} < 0 . \]  

where \( \Delta x_{j,ua} = 0.5 (\Delta x_j + \Delta x_{j,u}) \). This momentum transport term is only directly relevant to TRAC-PF1/MOD1 [6] and earlier codes. TRAC-PF1/MOD2 [7] currently uses area and macroscopic density weighing within this expression, but these terms are still under study and will not be discussed here.

For the flow model given by Eqs. (1)-(3), the combination of basic and stabilizer equation sets can be written in several ways. One ordering that is always stable begins with the stabilizer step for the equations of motion, is followed by a solution of the basic equation set for all equations, and ends with a stabilizer step for the mass and energy equations. For this ordering, the SETS finite-difference equations for Eqs. (1)-(3) are:

**STABILIZER EQUATION OF MOTION**

\[ \frac{(\bar{p}_{j,ua}^{a+1} - \bar{p}_{j,ua}^a)}{\Delta t} + V_{j,ua}^a \bar{p}_{j,ua}^{a+1} \]
\[ + \frac{\beta}{1} \left( \frac{\bar{p}_{j,ua}^{a+1} - \bar{p}_{j,ua}^a}{\Delta x_{j,ua}} \right) \bar{p}_{j,ua}^a \]
\[ + \frac{1}{\Delta x_{j,ua}} (\bar{p}_{j,ua}^a - \bar{p}_{j,ua}^*) \]
\[ + \bar{K} \left( \bar{p}_{j,ua}^{a+1} - \bar{p}_{j,ua}^a \right) \left| V_{j,ua}^a \right| = 0 . \]  

where
\( \beta = 0, \nabla_{j_{u_{2}}} \rho_{a} < 0 \)

1. \( \nabla_{j_{u_{2}}} \rho_{a} > 0 \)

BASIC EQUATIONS

\[
\frac{(V_{j_{u_{2}}}^{n+1} - V_{j_{u_{2}}}^{n})}{\Delta t} + V_{j_{u_{2}}}^{n} \nabla_{j_{u_{2}}} \rho_{a}^{n+1} \\
+ \beta \left( V_{j_{u_{2}}}^{n+1} - V_{j_{u_{2}}}^{n} \right) \nabla_{j_{u_{2}}} \rho_{a} \\
+ \frac{1}{(\rho_{j_{u_{2}}}^{n} \Delta s_{j_{u_{2}}})} (\rho_{j_{u_{2}}}^{n+1} - \rho_{j_{u_{2}}}^{n}) \\
+ \rho_{j_{u_{2}}}^{n} (2 V_{j_{u_{2}}}^{n+1} - V_{j_{u_{2}}}^{n}) \mid V_{j_{u_{2}}}^{n+1} = 0 \]

\[
(\rho_{j_{u_{2}}}^{n+1} - \rho_{j_{u_{2}}}^{n}) / \Delta t + \nabla_{j} \cdot (\rho_{a} \nabla_{j_{u_{2}}}^{n+1}) = 0 \;
\]

\[
(\rho_{j_{u_{2}}}^{n+1} \epsilon_{j_{u_{2}}}^{n+1} - \rho_{j_{u_{2}}}^{n} \epsilon_{j_{u_{2}}}^{n}) / \Delta t + \nabla_{j} \cdot (\rho_{a} \epsilon_{j_{u_{2}}}^{n+1}) \\
+ \rho_{j_{u_{2}}}^{n+1} \nabla_{j} \cdot (\mathbf{v}^{n+1}) = 0 \;
\]

and

STABILIZER MASS AND ENERGY EQUATIONS

\[
(\rho_{j_{u_{2}}}^{n+1} - \rho_{j_{u_{2}}}^{n}) / \Delta t + \nabla_{j} \cdot (\rho_{a} \mathbf{v}^{n+1}) = 0 \;
\]

\[
(\rho_{j_{u_{2}}}^{n+1} \epsilon_{j_{u_{2}}}^{n+1} - \rho_{j_{u_{2}}}^{n} \epsilon_{j_{u_{2}}}^{n}) / \Delta t + \nabla_{j} \cdot (\rho_{a} \epsilon_{j_{u_{2}}}^{n+1} \mathbf{v}^{n+1}) \\
+ \rho_{j_{u_{2}}}^{n+1} \nabla_{j} \cdot (\mathbf{v}^{n+1}) = 0 \;.
\]
A tilde above a variable indicates that it is the result of an intermediate step and is not the final value for the time step.

The material Courant stability limit is eliminated by treatment of the terms $V_{V', V'}$, $\nabla p V'$, and $\nabla p e V'$ during the two steps. Additional stability has been obtained with the particular form for the friction terms and the use of nonzero values of $\beta$ in the $V_{V', V'}$ terms. These special terms for friction and $V_{V', V'}$ are obtained by linearizing similar terms that are fully implicit in velocity.

Equation (7) simply represents a tridiagonal linear system in the unknown $V^{n+1}$ and is solved first. Next, the coupled nonlinear system given by Eqs. (9) - (11) is solved. In practice this is accomplished by a Newton iteration in which the linearized equations are reduced to a linear system involving only pressure variations (see Ref. 6 or 7). Once these equations are solved, $V^{n+1}$ is known; hence, Eqs. (12) and (13) are simple tridiagonal linear systems, with unknowns $\rho_j^{n+1}$ and $\rho_j^{n+1} e_j^{n+1}$, respectively.

When this equation set is adapted to flow in complex piping networks, the pure tridiagonal structure is lost. However, the matrices are still sparse and easily solved.

Recent Boiling Water Reactor (BWR) versions of TRAC use a numerical method very similar to that described above. However, there are two significant differences in TRAC-BFI [8] and later versions. Equation (7) is eliminated and Equation (6) as applied to the basic motion equation is replaced by

$$ V_{j+1,2} - V_{j-1,2} V = V_j^{n+1} (V_j^* - V_j^{n+1}) / \Delta x_{j,12}, \ V_{j,1} > 0 $$

$$ V_{j+1,2} - V_{j,12} V = V_j^{n+1} (V_j^* - V_j^{n+1}) / \Delta x_{j+1,2}, \ V_{j,1} < 0 $$

(14)

This mixture of new and old time velocities in the velocity gradient can result in a failure of the solution of the difference equations to converge to the solution differential equations as the time-step and mesh length approach zero. This convergence problem is discussed in detail in Section 5.

Relap5/Mod3 contains options for either a semi-implicit method or a variation on SETS referred to as the Nearly-Implicit method [9]. The semi-implicit option is similar to applying Equations (9)-(11) with the tilde removed from the velocities. The key difference with TRAC is that a full solution of these nonlinear coupled algebraic equations is not attempted. The linearized equations are solved once. The resulting values for variables are substituted into the right hand side of the rearranged mass and energy equations,

$$ \rho_j^{n+1} = \rho_j^n - \Delta t \nabla \cdot (\rho v^{n+1}) $$

(15)
\[ \rho_j^{n+1} e_j^{n+1} = \rho_j^n e_j^n - \Delta t \nabla_j \cdot (\rho^n e^n V_j^{n+1}) \\
+ \Delta t \frac{\beta_j^{n+1}}{\rho_j^n \Delta x_j} V_j \cdot (V^{n+1}) \tag{16} \]

To obtain the final new time densities and energies. This method eliminates systematic mass errors that can result from the solution of linearized equations alone. This type of corrector method was abandoned in the TRAC program in the late 1970's as being significantly less robust than an iterative solution of the nonlinear difference equations. However, differences in flow equations and details of numerical techniques may have made this a good choice for RELAP5.

For the model equations in this paper the Nearly-Implicit approach involves the elimination of Equation (7) and the replacement of Equation (9) with

\[ \left( V_{j,12}^{n+1} - V_{j,12}^n \right) / \Delta t + \frac{\beta_j^n}{\rho_j^n \Delta x_j} V^n + \frac{1}{\rho_j^n \Delta x_j} \left( \beta_j^{n+1} - \beta_j^n \right) \]

\[ = \frac{\rho_j^n \beta_j^n \Delta x_j}{\left| V_{j,12}^n \right|} = 0 \tag{17} \]

In the actual RELAP implementation the \( V \nabla V \) term is area and density weighted and central differenced. Equations (10) and (11) are linearized and solved to obtain intermediate values for new time pressure and specific internal energy as a linear function of cell face velocities. These relationships for pressure are substituted into Equation (17) and the resulting tridiagonal (for two-fluid equations a block tridiagonal) linear system is solved to obtain the new time velocities. Solution of Equations (12) and (13) follows as described for TRAC to obtain final new time densities and energies. At first thought, the conservative nature of the stabilizer mass equation should eliminate the need for anything other than a linearized solution of the basic equation set. Unfortunately, numerical experiments with TRAC indicate that stable behavior of the SETS method at high multiples of the material Courant limit requires a well converged solution of the nonlinear basic equation set.

The Nearly-Implicit method was originally described as a variant of SETS in reference [3], but rejected for use in TRAC because the use of separate stabilizer momentum equations actually requires less computational effort with TRAC's two-fluid equations. However, due to the presence of virtual mass terms in the RELAP two-phase momentum equations, the version of SETS found in TRAC is not feasible for RELAP and the Nearly-Implicit method is a natural choice.

RETTRAN [10] is a third safety code series commonly used in the USA, although for a narrower range of transients than TRAC and RELAP. A significant reason for this restricted utility is the continued use of a drift-flux formulation for the flow equations. RETTRAN-O2 uses a fairly conventional semi-implicit technique, but with mass flow replacing velocity as an independent variable. RETTRAN-O3 removes the material Courant limit with a fully implicit treatment of all mass, energy and momentum
flux terms [11], and applies advanced sparse matrix techniques to the resulting linear equations. It contains the option to solve only a single linearization of the equations as in RELAP, or to continue to solve the nonlinear equations with a Newton iteration. As is the case for TRAC and RELAP, RETRAN-03 and its predecessors evaluate all heat transfer and friction (wall and interfacial) coefficients at the old time level, and share the stability problems associated with this practice.

3. STABILITY

The original SETS method was constructed from information propagation arguments. It was observed that the semi-implicit approach eliminated the sound speed from the standard Courant limit of explicit schemes by transmitting sound wave information throughout the spatial finite-difference mesh in a single time step. The remaining material Courant stability limit \( \Delta t < \Delta x / |V| \) is accounted for by the argument that information on the material being convected is only propagated one cell per time step. Therefore, it was concluded that the remaining material Courant limit could be removed by adding to a semi-implicit method a step that propagates the necessary information on mass, energy, and momentum flux. This heuristic stability analysis is not always valid [12], and must be confirmed with more detailed linearized analysis and computational tests.

A more rigorous understanding of the stability of semi-implicit methods can be obtained by combining Equations (10) and (11) with the simple motion equation

\[
\frac{v_{j+1}^{n+1} - v_{j}^{n+1}}{\Delta t} + v_{j}^{n} \frac{v_{j+1}^{n} - v_{j}^{n}}{\Delta x} + \frac{1}{(\rho_{j}^{n})^{1/2}} \frac{p_{j+1}^{n} - p_{j}^{n}}{\Delta x} = 0. \tag{18}
\]

For a standard linearized stability analysis, the eigenvalues of the amplification matrix are

\[
\lambda = \frac{\beta - \eta}{\beta} \tag{19}
\]

and

\[
\lambda = \frac{\beta - \eta}{\beta} \tag{20}
\]

where
\[ \beta = \frac{\Delta x}{V\Delta t} , \]  
\[ (21) \]

\[ \eta = 1 - e^{-\Delta x} . \]  
\[ (22) \]

and

\[ \zeta = \eta \eta^* = 4 \sin^2 \left( \frac{k\Delta x}{2} \right) . \]  
\[ (23) \]

Also note that \( V > 0 \) has been assumed.

For the first pair of eigenvalues stability requires

\[ \lambda \lambda^* = \frac{\beta^2 - \chi(1 - \beta)}{\beta^2 + \frac{c^2}{V^2} \chi} < 1 . \]  
\[ (24) \]

This condition is clearly met for any \( \Delta t \) when the flow is subsonic, and as the velocity becomes much greater than the sound speed monotonically approaches the condition that \( \Delta t < \Delta x/V \). For the final eigenvalue the condition for stability becomes

\[ 1 + \zeta \frac{(1 - \beta)}{\beta^2} < 1 , \]  
\[ (25) \]

which is only true if \( \Delta t < \Delta x/V \).

Extension of this type of stability analysis to the SETS Eqs. (7)-(13) is not as simple as in the preceding cases. The algebra becomes more complicated and the eigenvalues of the amplification matrix are given by
\[
\lambda = \frac{\beta}{\beta - \eta}
\]  

(26)

and

\[
\lambda = \frac{2\beta(\beta + \eta) + r\eta^2(2\beta + \eta)^2 + 4r\beta(\eta^2 + 2\beta\eta^2 - \beta\eta)^2}{2(r + 1)(\beta + \eta)^2}
\]

(27)

The magnitude of this eigenvalue is difficult to evaluate. However, it is instructive to take the limit of time steps much greater than the material Courant limit. For this case, \(\beta\) approaches zero and the eigenvalues in Eq. (27) go to zero and

\[
\lambda \rightarrow \frac{r}{r+1} < 1
\]

(28)

suggesting unconditional stability.

In practice, the SETS equations are not unconditionally stable because heat transfer and friction coefficients are evaluated at the old time level. At very large time steps, functional forms for the friction factor may result in a strong velocity dependence, and can lead to instabilities, as can a strong void-fraction dependence for interfacial friction in the two-fluid model. This is why the method is referred to as stability enhancing rather than unconditionally stable.

One simple illustration of the instabilities that can arise from explicit coefficients is the problem that initially prompted the peculiar linearized implicit friction terms (wall and interfacial) in TRAC versions later than PD2. Consider a test problem consisting of a 5 foot high vertical column with a stream of air bubbles injected at the bottom. The problem is started in a pure liquid state and run to a steady state two-phase bubble rise flow. For this example the column is divided into 20 equal length cells. When run with the standard release version of TRAC-PFI/MOD2, it runs stably at all time steps tested (.001 - .5 s ). A second series of runs has been made with a special version of TRAC-PFI/MOD2 in which the interfacial drag terms are the product of an explicitly evaluated coefficient, the absolute value of the relative velocity evaluated at the old time, and the relative velocity evaluated at the new time level. This is the form found in the vessel component of TRAC-PD2 and in all known versions of RELAP5.

Figure 1 shows the vapor velocity at the bottom face of the top cell in the column for a stable run with a time-step of .001 seconds, and for an unstable run with a time step of .02 seconds. For this problem the material Courant limit is about .5 seconds. The instability onset appears at about .01 seconds, and time-step control in TRAC will not permit execution of the problem with step values
consistently much above .02 seconds. When examined in detail the vapor velocity is switching
between two extreme values from one step to the next.

![Graph: Vapor Velocity vs Time](image)

**Figure 1. Bubble Rise Velocity**

Figure 2 illustrates an important feature seen consistently in problems containing this type of
interfacial drag instability. The mean void fraction in the column attains a steady value below that
of the stable case. The average value of the bubble rise velocity in the unstable regime is larger than
the actual bubble rise velocity. This oscillating instability has affected the accuracy of the prediction
of the mean behavior of the system. Although the fix introduced in TRAC-PF1 appears to have solved
the problem for all interfacial drag correlations used in the code. One could imagine correlations with
more dependence on relative velocity introducing further instability due to old time level evaluation.

Other instabilities are clearly present in these codes due to explicit evaluation of various coefficients.
Both TRAC and RELAP have had long term problems with time-step sizes being forced down to
unexpectedly low values during the modeling of reflood. I believe that the prime cause in this
situation is an unstable subcooled boiling model, but old time evaluation of wall and interfacial heat
transfer coefficients are probably also playing a role. Even in seemingly stable conditions there is
evidence [13] that fully explicit evaluation of wall heat transfer coefficients can lead to slow growth
instability that significantly affects results, but is difficult to detect.
Although various forms of formal stability analysis are useful, only careful numerical experiments provide to final answers on numerical stability limits and the sources of some subtle instabilities. A common test is to drive a simple one dimensional channel with either two pressure boundary conditions or a combination of a mass flow and a pressure boundary condition. This is often fruitful, as in the above example. However, one should recognize that certain instabilities can be swept out of the exit pressure boundary condition. A more sensitive test is to construct a closed loop with either constant initial velocity and no wall friction or wall friction and a momentum source in one or more cells.

4. DIFFUSION

Because SETS was treated simply as an addition to the semi-implicit method, no attempt was made to modify the original spatial and temporal differencing procedures. As a result, the current form of SETS is first-order accurate in time and space and in some instances can produce excessive artificial diffusion. Fortunately, this diffusion is not often a problem in modeling reactor transients. The positive side effect of this low-order accuracy is that the method is extremely robust.

The existence of this strong numerical diffusion in codes with donor-cell differencing is well known. To provide a specific example, a test problem has been run using the boron field in TRAC-PFI/MOD2. A Boron solution is injected at time zero into a 10 meter pipe containing liquid moving at 1 meter per second. The boron mass fraction in the solution is 0.002. The pipe is initially modeled with 20 cells 0.5 meters in length. Figure 3 shows boron concentrations profiles at various times for a calculation with constant time steps of 0.250 seconds (half of the material Courant limit). Much of the numerical diffusion in this example is due to the implicit nature of the SETS method. In fact for
this problem with a constant velocity, the numerical diffusion is the same as that for a fully-implicit method using the same spatial differencing. Figure 4 contains the results of the same run with the stabilizing mass and energy equations eliminated. The code is running in semi-implicit mode, and has significantly less numerical diffusion. Numerical diffusion in this case is equivalent to that of a standard explicit method running with constant velocity.

![Graph showing position vs. time for different times](image)

**Figure 3. Implicit Diffusion Test, Time Step = .25s**

Figures 5 and 6 illustrate the time-step size sensitivity of the implicit (SETS) and explicit (semi-implicit) time differencing scheme. As should be expected, the diffusion in the implicit method increases monotonically with time-step size. For the explicit numerical scheme, truncation terms associated with old time level evaluation of density in the mass flux tend to cancel the diffusive terms associated with donor-cell differencing. As should be expected, the boron profiles approach the same limit for the implicit and explicit methods as time-steps become small. For the explicit flux terms, diffusion decreases with increasing time-step size until a step function is propagated perfectly at the material Courant stability limit. In practice this non-diffusive propagation can not be achieved due to the standard use of a non-uniform mesh and the use of a variety of controls on time-step size.

Figure 7 illustrates the effect of mesh size on diffusion for implicit and explicit fluxes. These results can also be predicted from the knowledge of the time and time-step dependence, and a remap of a given profile to half the time value on a mesh with half the cell length.
Figure 4. Explicit Diffusion Test, Time Step = 0.250 s

Figure 5. Implicit Diffusion Test, t = 6 s
Figure 6. Explicit Diffusion Test, $t = 6s$

Figure 7. Diffusion Test, $t = 6s$, Time Step = .100s
A misconception exists with some people that the numerical diffusion in a donor-cell method will damp out oscillations, making it inappropriate for the analysis of phenomena such as BWR core oscillations. Figure 8 presents a counter example to this assumption, plotting the time-history of the liquid level in a manometer. The manometer is modeled with 40, .05 meter cells. In fact Borkowski [14] has produced some very useful BWR predictions using TRAC-BF1. Numerical diffusion can act to damp high amplitude manometer oscillations down to a lower level that is maintained without further damping. However, the most frequent phenomena in codes like TRAC that act directly to suppress oscillations are related to water-packing [15]. Problems in thesis research by B. Boyer on flow instabilities in condensers [16] were traced to this source.

![Manometer Liquid Level](image)

Figure 8. Manometer Liquid Level

Numerical diffusion can have an effect in the modeling of problems such as BWR core oscillations through another source. If a liquid front is artificially spread beyond it's true location in a core rod region, the underlying driving force may be incorrectly modeled to the extent that oscillations are never seen. These spreading effects tend not to be as severe in TRAC and RELAP as those spreading a boron front in a uniform velocity. The action of gravity combined with the non-uniform effects of interfacial drag on the liquid drop and vapor bubble velocities tend to keep fronts sharper than might be expected. The bubble rise stability test in Section 3 is a good example of this lowered amount of diffusion. The correct time for vapor arrival in the top mesh cell is 10.4 seconds. The first vapor is seen in this cell with a full SETS calculation at about 10.0 seconds for all time steps used (.001 - .5 s). In the case of the manometer oscillation presented in this section, no significant spreading of the liquid interface is seen.

At this time the basic versions of these system analysis codes continue to employ donor-cell spatial differencing. However, modifications to RETRAN-03 have been published that include a special
difference method to reduce numerical diffusion [17]. This method is applied to the full set of
difference equations in RETRAN-03 and shows significant promise. An in-house version of TRAC
includes a second-order Godunov method [18] to remove diffusion from the boron equation, but
continues to use a standard full donor-cell difference in all other equations. This is scheduled for
release in TRAC-PFI/MOD3. Similar improvement to the boron field in RELAPS should be expected.

5. CONVERGENCE

There are actually two different convergence problems faced by these codes. The first is simply the
convergence of the iteration required to solve the nonlinear coupled difference equations, and only is
an issue for the TRAC series and the non-linear option in RETRAN. The Newton iteration in TRAC
has always converged well provided that excessive changes in independent variables are limited by
time-step control and tests are included to deal with transitions across the saturation line and between
single and two-phase states. The second convergence problem is the question of whether the solutions
to the difference equations converge to those of the differential equations as the time-step and mesh
length approach zero. This property is also referred to as consistency.

The first thing to note regarding TRAC and RELAPS is that it is not desirable to operate in a state
where the difference equations are too close to full consistency with the differential equations. It is
well known that the two-fluid model used in these codes is formally ill-posed [19]. Fortunately, at
any practical time-step or mesh size, non-physical solutions are suppressed by the current difference
equations. This issue should be reconsidered by anyone developing higher-order difference methods
for reactor safety codes. It may be necessary to use a set of partial differential equations that are well-
posed.

The most obvious occurrence of a consistency problem is in the TRAC-BWR motion equation. The
difficulty can be illustrated with a simple flow equation

$$\frac{\partial V}{\partial t} + V \frac{\partial V}{\partial x} + \frac{1}{\rho} \frac{\partial p}{\partial x} = 0.$$  \hspace{1cm} (29)

The corresponding difference equation (assuming positive flow) in the BWR numerical method is

$$\frac{V^{n+1}_{j+1/2} - V^n_{j-1/2}}{\Delta t} + V^n_{j+1/2} \frac{V^{n+1}_{j+1/2} - V^n_{j+1/2}}{\Delta t} + \frac{1}{(\rho^n_{j+1/2})} \frac{(p^{n+1}_{j+1/2} - p^n_{j+1/2})}{\Delta x} = 0.$$  \hspace{1cm} (30)

Now apply a standard truncation error analysis to Eq. (30), using Taylor series expansions about time
level n and space point j+1/2. The result is

$$(1 + \frac{\Delta t}{\Delta x} V^n_j) \frac{\partial V}{\partial t} + V^n \frac{\partial V}{\partial x} + \frac{1}{\rho} \frac{\partial p}{\partial x} + O(\Delta t) + O(\Delta x) + \ldots = 0.$$  \hspace{1cm} (31)
Consistency with the original partial differential equation clearly requires a time step size substantially below the material Courant limit.

The developers of TRAC-BWR were aware of this situation when the method was installed, but after careful consideration concluded that analysis of transients for which the code was designed would not be significantly affected. In most instances the velocity field is in a quasi-equilibrium state, or other conditions such as reflood or ECC injection limit the time-step well below the Courant limit. However, a user should always be aware of the limitations of this hybrid method. For example, it should not be applied to problems where the rate of coast-down after a pump trip or the time for establishing full natural circulation is important. In cases such as these, a standard SETS approach is clearly preferable.

A more subtle consistency problem is related to the modeling of discontinuities. One example of this is the classic water-packing problem [15]. If a water-packing pressure spike occurs on a given mesh, reduction of the time-step actually increases the magnitude of the spike. A second example is modeling of flow through an abrupt area change. Given the non-conservative form of the energy equation in TRAC and RELAP5, it is not possible to simultaneously obtain correct values for both the pressure and temperature change across an abrupt area change regardless of how small the time-step and mesh size. This is at the root of recent safety concerns, regarding the use of RELAP5/MOD3 to model both the reactor and containment (a job it was never meant to perform). This form of the energy equations also prevents the codes from correctly modeling the propagation of shock waves, but they have never been intended for that task.

6. CONCLUSIONS

The numerical methods in TRAC and RELAP5 have evolved to the point that they are very similar and share many advantages and flaws. The RETRAN series has followed a different path and now contains more implicit difference equations, and better options to counter numerical diffusion. Unfortunately RETRAN has continued to use a drift-flux formulation for it’s basic equation set, limiting the range of transients that it can validly model.

Even after 15 years of development the TRAC and RELAP programs have numerical stability problems that should be resolved. The explicit treatment of friction and heat transfer coefficients is undoubtedly increasing the computer time required for simulating many transients. It may also be affecting the accuracy of these simulations.

REFERENCES


ADVANCED NUMERICAL METHODS FOR THERMALHYDRAULICS
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ABSTRACT

New applications have been found for advanced best estimate codes, due to the needs for industrial studies and safety studies, and due also to the appearance of new generations of computers, as powerful work stations. Thus developments have been focused on the optimization of the numerical performances of thermal-hydraulics codes based on two-fluid six equation model.

The first field of investigation is to minimize the computing time. Comparisons of existing methods demonstrate that a fully implicit scheme is suitable to be optimised.

The second field of investigation is to develop powerful three-dimensional models with advanced numerical method, as multistep method, conjugate gradient method for the solver and in future non-structured meshing.

1. INTRODUCTION

Several thermalhydraulics codes of second generation have been developed for the last ten years. Two fluid six equation model are involved in these codes where one or three dimensional modules are used. These codes are operationnal and have proved their efficiency in a wide range of applications in research and development studies and in safety studies.

CATHARE is the best estimate code, developed by EDF, FRAMATOME and CEA. It used for Research and Development, licensing and safety studies by the French partners. It is also widely used by industrials and safety institutes in several countries. The latest version which has been released is the version 1.3. A new version 1.4 is under development, following the investigations discussed before.

The French partners have defined new applications for CATHARE as follow:
* The code is used for licensing by Electricite de France (EDF) and FRAMATOME. A methodology is under development, which includes the development of tools for sensitivity studies.
* The use of CATHARE for safety studies implies that the code is aliving, it means under-development in order to still have a development team which is able to act as expert for the industry as for the safety authority in the use of the code.
* The code will be coupled with larger industrial code systems where several physical domains are involved (mechanics, chemistry, neutronics, ...) For instance, the code will take part into the EEC severe accident code ESCADRE.
* A new generation of fast running industrial simulators with a best estimate thermal-hydraulics module is under development. CATHARE will be included in the new EDF SITPA simulators as in the new EDF full-scale simulators.
* CATHARE has to provide an advanced three-dimensional module with qualified physical models and with a low CPU-time consuming.
2. TWO FLUID SIX EQUATION MODULE

2.1 OPTIMIZATION OF EXISTING CODES

The objective of this investigation is to drastically reduce the CPU-time needed for the thermohydraulics codes. The CPU time used by a code for a given calculation can be written:

\[ \text{TCPU} = \text{ITERATION} \times (\text{NUMBER OF MESHES}) \times (\text{ELEMENTARY TIME}) \]

There are two ways to reduce TCPU: either to reduce the elementary time, or to reduce the number of iterations needed to calculate a given physical situation.

The elementary time depends on two factors, the numerical method used in the discretization of the non-linear system, and the optimization of the Fortran coding. For a given numerical algorithm, the number of Newton iterations performed by the code can be minimized by reducing the number of numerical incidents.

2.2 OPTIMIZATION OF A ONE-DIMENSIONAL MODULE

This module is based on a 6-equation 2-fluid model (2 mass equations, 2 energy equations and 2 momentum equations). 2 additional transport equations can be used for the noncondensable gases. Boron and activity transport can be also calculated. Investigations were made to compare several advanced numerical schemes with implicit behaviour. Then, three modules were developed, one with a fully-implicit scheme, and the two others with multi-step method where the solution scheme of equations is split into fractional steps:

a) A fully implicit scheme. developed for the CATHARE code(1), (the interphase exchange, the pressure propagation and the convection terms are totally implicitly evaluated) have been used to achieve the largest time step as possible which is not Courant limited. The solution of the non-linear difference equations is solved by a Newton-Raphson iterative method. In this method, the analytical derivatives for each constitutive relationship and each equation is systematically used. There is no use of numerical derivatives.

b) A nearly implicit scheme

The first step consist of solving all six equations, treating all interphase exchange processes, the pressure propagation process and the momentum convection process, implicitly. The mass and energy equations are solved locally in a cell to give a single pressure equation depending only on the phasic velocity at each face of the cells. The pressures are eliminated and the velocities are obtained from the momentum equations.

The second step is used to implicitly evaluate the convective terms in the mass and energy balance equations.

c) The HITE scheme

In this method, developed by HITEFFY(2), three steps are used for the semi-explicit basic step, as follow:
* the pre-prediction for velocities
* the velocity convection stabilization step
* the semi-implicit step involving implicit pressure

Then, the convective terms in the scalar equation are evaluated implicitly in the mass and energy balance equation.
<table>
<thead>
<tr>
<th>Task in the module</th>
<th>CATHARE 2 V1.3</th>
<th>fully implicit</th>
<th>nearly implicit</th>
<th>SETS</th>
</tr>
</thead>
<tbody>
<tr>
<td>Discretization</td>
<td>468</td>
<td>200</td>
<td>187</td>
<td>75</td>
</tr>
<tr>
<td>Constitutive Relationships &amp; steam/water</td>
<td>211</td>
<td>57</td>
<td>57</td>
<td>27</td>
</tr>
<tr>
<td>Management</td>
<td>229</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>TOTAL</td>
<td>908</td>
<td>257</td>
<td>244</td>
<td>102</td>
</tr>
</tbody>
</table>

**TABLE 3 COMPARISON OF CATHARE WITH HIGHLY VECTORIZED MODULES**

| ELEMENTARY TIMING (μs) |

These three modules were developed with an high level of vectorization and were used to calculate the CANON VERTICAL experiment. It is a blowdown experiment (2) enabling quantification of the interfacial friction and vapor generation. It is initiated at nominal FWR pressure (15 MPa) and a 1 to 10 mm diameter break is opened. At the beginning the fluid is single-phase liquid. During the transient a large range of single and two-phase flow behaviors is covered. (boiling, swell level, critical flow) Thus, it is suitable for testing numerical methods. This experiment is modeled with a pipe of 59 meshes. The transient was computed until the test section becomes empty in the four cases. The results of the behavior of the three modules are compared to CATHARE 2 Version 1.3 (Table 2).

The data management task in CATHARE is highly time consuming and will be drastically reduced in future versions. The constitutive relationships and the steam/water physical properties can be easily vectorized. In the SETS method, the low timing for this task is due to the lack of some derivative computations. The discretization task includes the computations of the equations (highly vectorized) but also the elimination and the resolution task. It is higher for the fully implicit method and lower for the SETS method. The differences between all the methods is due to the time consuming for inverting large matrix. The physical solution was identical for CATHARE, the fully implicit method and the nearly implicit method, but for the SETS method large oscillations were discovered due to sharp interfacial friction variation, which is consistent with the results obtained by SPINDLER et al. (4).

For future developments, it was chosen to keep on with the fully implicit method and to investigate advanced method for matrix inversion, to drastically reduce the matrix calculations. Considering that a wide range of physical situations are supposed to be calculated, as for example accident management and small break local in nuclear safety, this method is well adapted when the optimization task is completed.

### 2.3 REDUCTION OF NUMERICAL INCIDENTS

Nevertheless, the experience has shown that the nearly implicit and fully implicit schemes have not been able to achieve as fast a running capability as might be expected. An explanation lies in the nature of the 2-phase problem, particularly the discontinuities that occur in the derivatives of the dependant variables.

When the algorithm has difficulty to converge, it can have an origin in the physical constitutive relationship behavior. The constitutive parameters associated with the exchanges of energy or momentum with the interface or the wall can all suffer rapid change in magnitude from one time step to the next. The flow regime transitions also result in nearly-discontinuous behaviors of the constitutive models. Another factor is the anomalous numerical behavior (e.g. water packing,...) which affects the implicit schemes, as the semi- or nearly-implicit
schemes. These anomalies effectively limit the achievable time step, but there are inescapable.

However, some incidents are due to either programming errors or discontinuity of function (or derivatives) or an incorrect use of algorithms, which could be corrected. In that way, a substantial effort has to be done to analyze the incidents and examine if it is possible to improve the robustness of a code. In particular, a systematic comparison between analytic and numerical derivatives are performed during the ongoing developments of CATHARE code to find out errors.

3. THREE-DIMENSIONAL MODULE

A three-dimensional module has been developed\(^\text{[5]}\). Its objectives are mainly to be able to describe, either the pressure vessel of a PWR during a Large Break LOCA or severe accident, or the containment building, or eventually other component. The module is also based on a six-equation two-fluid model, with eventually additional equations for non-condensable gases and for boron or activity transport.

3.1 Discretization of the equations

As for the one-dimensional module, to have a stable numerical scheme with sufficient damping, the equations are discretized at the first order in using a finite difference scheme with staggered spatial mesh and a donor-cell method.

A multi-step method with implicit behavior is used to resolve the system, in order to eliminate the material courant limitation. In the case of the three-dimensional module, a fully implicit method would be too much time consuming, due to the large matrix inversions needed.

a) Predictor step

The predictor step is close to a semi-implicit method. The convective terms are taken implicitly if the fluid enters the cell, explicitly if not. The source terms are taken implicitly. The mass and energy fluxes are calculated with implicit velocities and donor explicit quantities for the scalar. In the momentum equations, the spatial derivatives and the interfacial friction are taken implicitly.

b) Corrector step

The corrector step ensures a correct mass balance.

c) Last step

A last step is necessary to calculate the main variables, the pressure, the partial pressure, the liquid or gas temperature and the void fraction, in resolving a system of four to eight equations (depending on the number of condensable gases). In fact, only one iteration is performed to get the void fraction.

3.2 CONJUGATE GRADIENT SQUARED ALGORITHM (6,7)

The conjugate gradient squared algorithm is a generalization of the conjugate gradient method, capable to deal with non-symmetric matrices. This iterative method was introduced by Sonneveld (7). A preconditioning of the system must be performed in order to accelerate the convergence rate of this iterative method. It is based on the incomplete LU factorization, for which two levels are presently available: the level 0 which preserves the fill-in structure of the original matrix A, and level 1 which typically adds 6 extra diagonals to the seven-diagonal matrix structure arising in finite difference 3D regular grids.

The important properties of this iterative method are:

- Extensive use of matrix-vector products and vector-vector dot products which are performed very efficiently on a vector computer.
- Limited memory requirements
- CPU total cost associated with the number of iterations needed to converge and with the balance between the effort spent for preconditioning and the resulting convergence acceleration.
On Table 6, comparison between LU direct method and Biconjugate gradient iterative method is compared in the case of a TAPIOCA calculation.

<table>
<thead>
<tr>
<th>SOLUTION METHOD</th>
<th>225 CELLS</th>
<th>450 CELLS</th>
<th>1000 CELLS</th>
</tr>
</thead>
<tbody>
<tr>
<td>DIRECT SOLVER</td>
<td>.550</td>
<td>1.980</td>
<td>8.780</td>
</tr>
<tr>
<td>LU FACTORIZATION</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>BICONJUGATE GRADIENT</td>
<td>.610</td>
<td>1.400</td>
<td>3.260</td>
</tr>
<tr>
<td>LU 0. PRECOND.</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>PERFORMANCE RATIO</td>
<td>.902</td>
<td>1.4</td>
<td>2.693</td>
</tr>
</tbody>
</table>

Table 6: TAPIOCA Blowdown Tests. Comparisons Between the Solvers

4 APPLICATION TO CATHARE CODE

4.1 PERFORMANCE OF CATHARE VERSION 1.3

As it has been mentioned before, contrary to most of the other industrial codes, the fully implicit scheme has been used from the beginning of CATHARE development. The time step advantage that fully-implicit schemes have, is partially offset by the fact that in two-phase systems, it is necessary to use rather small time steps at points of discontinuity in the constitutive models and at points of phase appearance and disappearance. It is also offset by the fact that the matrices to be solved are very large.

<table>
<thead>
<tr>
<th>CALCULATIONS</th>
<th>BETHSY small break 6 inches</th>
<th>BETHSY small break 6 inches</th>
<th>PWR Large Break</th>
<th>PWR Large Break</th>
</tr>
</thead>
<tbody>
<tr>
<td>EXPERIMENTAL TIME</td>
<td>1159</td>
<td>1151</td>
<td>542</td>
<td>86</td>
</tr>
<tr>
<td>(s)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>NUMBER OF HYDR. MESHES</td>
<td>301</td>
<td>301</td>
<td>216</td>
<td>216</td>
</tr>
<tr>
<td>NUMBER OF ITERATIONS</td>
<td>29099</td>
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<td>92741</td>
<td>42072</td>
</tr>
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<td>CPU TIME (s)</td>
<td>9100</td>
<td>17114</td>
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<td>ELEMENTARY TIME</td>
<td>1040</td>
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<tr>
<td>(us)</td>
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</tr>
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<td>COMPUTER</td>
<td>CRAY2</td>
<td>HP9000-720</td>
<td>CRAY 2</td>
<td>CRAY2</td>
</tr>
</tbody>
</table>

Table 2: Performances of CATHARE 2 Version 1.3 during Computations of Integral Systems

In CATHARE, the time-step is automatically managed and depends strongly on the nature of the physical phenomena which are modelled. On Table 2, the elementary time (CPU-TIME/ITERATIONS/MESH CELLS) is presented for different calculations: two simulations of small break loca performed on the BETHSY test facility(8), two PWR large break calculations (one performed without non-condensable gases and including all the reflooding phase and one with HYDROGEN and NITROGEN gases). It can be
noticed the impact of the non-optimized elimination process in comparing the two large break calculations. On the table 3, the repartition between the different tasks performed by the code is compared. It is noticed that the discretization, the constitutive relationships and the steam-water properties are very time consuming which proves that the coding is not optimized. Due to the implicit scheme, the elimination and resolution tasks also result in a large CPU load.

The code runs without major numerical problems. However numerical incidents, which lead to time step reductions, can occur during the transient. There are essentially due to some physical parameters which are out of range of validity during the iterations or some difficulty for the code to find a physical solution. Then the code reduces the time step and restarts the iterative process. It has been noticed that 30% of the iterations are lost.

4.2 Coding optimization for the VERSION 1.4

In a fully implicit code, the linear algebra relating to the Jacobian matrix should be the dominant CPU-time cost, contrary to the performances analysis shown in Table 3. Thus, it has been chosen to design a completely new internal data structure in order to take advantage of intensive vector computing and to minimize memory movements. It has to be noticed that this task is achieved without any change in the constitutive relationships and in the 5 equations (as in their discretization). Only coding aspects are taken into account, in such a way that version 1.4 strictly reproduces the physical results of version 1.3. Therefore full vectorization of the code was put as a base requirement for coding.

When the objective of drastically reducing the CPU-time spent in the equations, the constitutive relationships and the information management, is achieved, parallel computing through multitasking aspects will be considered to speed up the system solution. At present time of v1.4 development, some important results can be already presented, concerning the optimization and the vectorization of the discretization, the constitutive relationships and of the steam-water properties. Comparisons have been made with a 3D meshed CAYON VERTICAL computation (table 4).
4.3 Numerical performances of the three-dimensional module

During this development, much attention has been paid to the problem of vectorization in order to make efficient use of vector computers. Most of the computing time can be distributed among three major task: the discretization, the physical constitutive relationships, the resolution of the Jacobian matrix.

<table>
<thead>
<tr>
<th>Task in the code</th>
<th>CATHARE 2 V1.3</th>
<th>First developments</th>
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</thead>
<tbody>
<tr>
<td>Discretization (1-D module)</td>
<td>259.</td>
<td>127.</td>
</tr>
<tr>
<td>Discretization (other module)</td>
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<tr>
<td>Constitutive Relationships</td>
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<td>31.</td>
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<td>Steam water phys. properties elimination of the internal Eq. of the modules</td>
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<td>Resolution of the system</td>
<td>177.</td>
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</tr>
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<td>Management of the information</td>
<td>229.</td>
<td>23</td>
</tr>
</tbody>
</table>

* not yet optimized

TABLE 4 REPARTITION OF THE TIMING FOR DIFFERENT TASKS IN CATHARE 2, PERFORMED ON CRAY XMP 28, CPF77: ELEMENTARY TIME (µs)

To demonstrate the robustness of this module, a TAPIoca (5) calculation which simulates the blowdown of a PWR pressure vessel, initiated at 15MPa. A 20mm lateral break is opened. The three-dimensional module give reasonable physical results, all the most that it is not yet qualified. On table 5, the numerical performances of this module are presented. When this effort is made, most of the computing time is spent in the resolution of these linear systems. A direct method (LU factorization) was used in a first attempt to solve the linear Jacobian system in the predictor as in the corrector. This method is expensive and an alternative approach has been recently implemented which is based on a conjugate gradient type method.

<table>
<thead>
<tr>
<th>DISCRETIZATION</th>
<th>CONSTITUTIVE RELATIONSHIPS</th>
<th>JACOBIAN RESOLUTION</th>
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<tr>
<td>17</td>
<td>11</td>
<td>473</td>
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</table>

TABLE 5 NUMERICAL PERFORMANCES OF THE 3D MODULE DURING A TAPIOCA BLOWDOWN CALCULATION (ELEMENTARY TIME IN µs).
5. CONCLUSION AND PERSPECTIVES

1. The fully implicit method offers, for a one-dimensional model, good possibilities to be optimized. The vectorization of the equations and the physical constitutive relationships is an easy task to reduce the CPU time. This can be achieved without any modifications of the physical results. Investigations are performed to reduce the cost of the linear algebra task and to reduce the physico-numerical incidents.

2. A three-dimensional module is developed. A multistep method is used to achieve an implicit behaviour. Its first performances are reasonable. Recent efforts were made to implement a resolution method based on the Preconditioned gradient, which can provide significant time saving for problems with more than 500 nodes. Multidimensional development will be enhance, in particular to develop non-structured meshing.

3. These investigations are applied to the new version of CATHARE under development. CATHARE is used in several related applications:
   * This new version will be fast running, in particular to be integrated in the new generation of simulators.
   * The flexibility will be improved, in particular its robustness because the code is used for industrial studies.
   * The code should be easily coupled with other codes, as severe accident codes.
   * A three-dimensional module is needed to represent pressure vessel, containment and eventually other components.

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SESSION 2

PROGRESSES IN THERMALHYDRAULIC EXPERIMENTS: LARGE SCALE PHENOMENA

Session Chairman:
M. Courtaud, CEA/DRN, Grenoble
In Session 2 entitled "Progress in thermalhydraulics experiments large scale phenomena", four papers were presented: two on UPTF, the 1/1 scale German facility, one on LOFT, the US experimental reactor, and one, more general, on scaling and counterpart tests. A paper on the large Japanese reflood experiments SCTF and CCTF was distributed, but was not presented nor discussed. UPTF is still in operation while LOFT, SCFT and CCTF have been dismantled or will be in the near future).

The first paper showed that the UPTF tests remarkably extended the data base required to develop and validate analytical models used in the large thermalhydraulics codes for the simulation of two-phase flow phenomena in full scale reactor geometry. Some conclusions were drawn with respect to the scalability of two-phase flow phenomena:

* in the loop the classical "J" scaling can be applied successfully,
* for inhomogeneous multidimensionnal flow, e.g. in the upper plenum or downcomer, there is a need for improved scaling due to spatial effects caused mainly by counter current flow of subcooled water and steam/entrained flow.

The second paper dealt with the analysis of UPTF data and presented similar conclusions. ECC injection performance cannot be simulated in a best estimate way in small scale facilities and UPTF data were needed.

The most promising method of scale up is calculation by computer code of integral facility behaviour. This requires adequate constitutive equations proven for the entire scale of code application.

A complete code validation processing scale up to full plant size is still pending. The first calculations for UPTF showed that computer codes having constitutive laws based on small scale test facilities were not able to predict full scale experiments. This was also observed for codes featuring a 3D representation of the reactor vessel like TRAC PFI. Some work has already been done to improve the codes in this respect and some work will still go on, in order to achieve better best estimate calculation results.

In the third distributed paper, which presented the large reflood experimental programmes SCTF and CCTF, the importance of 3D phenomena was also outlined. The three dimensional phenomena provide better core cooling in a high power bundle than expected from models based on one dimensional reflood experiments. For a complete description of the phenomena, more precise modelling of two-phase flow is necessary.

Significant large scale phenomena identified in the LOFT experiments were presented in the fourth paper. LOFT, an experimental reactor, was an unique source of integral data and provided the nuclear reactor safety community with a very important source showing the importance of integral testing and identifying behaviour or phenomena not shown previously in separate effect tests or smaller integral tests.
Three examples were presented for this service:
- in the blowdown phase of large break LOCA, sudden quenching was observed and no mechanistic model was available to describe these phenomena. Safety codes currently apply an empirical heat transfer correlation based on the transition to nucleate boiling. Code calculation of the LOFT experiments indicate that this is not a very satisfactory procedure;
- code analysis of four small break LOCA experiments showed that the flow regime maps and the branching models were inadequate for proper simulation of the key phenomena responsible for coolant inventory during the transients;
- the results of the reflow effects during severe accident transients were unexpected and stimulated intensive model developments to enhance code prediction capabilities. Quenching of the degraded core following water injection is clearly important for accident management. A proper understanding and modelling of the processes is therefore necessary and presently lacking.

Data from LOFT - the largest nuclear integral test facility - form the most important data base for the assessment of system codes to predict nuclear power plant under accident conditions.

The 5th paper treated more generally of scaling problems and counterpart tests. The following conclusions were drawn:
- in order to perform scaling analysis in integral test facilities, it is necessary to demonstrate that similar phenomena occur at different scales.
- even if the above condition is fulfilled, differences in hardware and in boundary conditions prevent direct extrapolation of data to the plant.
- the use of system codes appears strictly necessary to perform scaling analysis. A procedure for scale analysis was proposed.
- counterpart tests are not strictly necessary for scaling but are very useful for increasing the cooperation and acquiring common understanding.

From the general discussion it was agreed that scaling could be easily applied for a given physical phenomenon using dimensional analysis. In integral test facilities however, many physical phenomena occur which can differ in relative importance according to the different scenarios to be treated. Scaling cannot be achieved in the same time for all phenomena and compromise is necessary. The behaviour of an integral test facility cannot be extrapolated to give nuclear plant behaviour. In order to do so, computer codes must be used, provided their constitutive laws contain the right scale effect which can be assessed on separate effect tests.

The large scale test facilities moreover, show in many cases the importance of multidimensional phenomena which could not exist at smaller scales. These phenomena can be described in some way with the current 1D case if they are adequately assessed. Nevertheless development of 3D codes is recommended, accompanied by sufficiently instrumented separate effect tests to allow their accurate assessment.

To assess new passive systems for future reactors, it will be necessary to extend the data base using modified existing test facilities or new ones. Finally, it is very important to preserve the data obtained at great cost in the large test facilities and which are indispensable for code assessment.
"Two-Phase Flow Phenomena in Full-Scale Reactor Geometry"

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Abstract

For the investigation of two-phase flow phenomena in full scale reactor geometry, a series of experiments were carried out at the Upper Plenum Test Facility UPTF, which represents the primary system of a 1300 MWe Pressurized Water Reactor with upper plenum, downcomer and primary main coolant pipes in 1 : 1 reactor scale.

UPTF was the German contribution to the international 2D/3D project established by the Japan Atomic Energy Research Institute (JAERI), the Nuclear Regulatory Commission (USNRC) of the United States of America, and the Federal Ministry for Research and Technology (BMFT) of the Federal Republic of Germany.

Large scale findings of the UPTF tests, related to two-phase flow phenomena in the downcomer, in the upper plenum, at the upper core tie plate, and in the main coolant pipes, will be discussed. The application of the UPTF test results for the validation of analytical models will be demonstrated.

1. Introduction

Contrary to single phase flow, where the flow consists of a uniform fluid, two-phase flow is characterized in general by large density and velocity differences between the water and the steam phases with various flow patterns, resulting from the interaction between surface tension, pressure drop, shear stress and gravity force.

An enormous number of experiments have been performed to investigate two-phase flow phenomena in the last two decades. However, the tests have mainly been executed at sub-scaled test facilities compared to full scale reactor geometry.

To provide required experimental data for the two-phase flow in reactor geometry, a test program at the full-scale test facility UPTF was executed [1].
2. The Upper Plenum Test Facility

The Upper Plenum Test Facility UPTF is an imitation of a four-loop 1300 MWe pressurized water reactor with upper plenum, downcomer and the main coolant pipes in full scale reactor geometry (Fig. 1 and 2).

The steam produced in a real core and the entrained water flow are simulated by a controlled steam and water injection through the core simulator. The steam generator behaviour is simulated by a controlled steam injection into the steam generator simulator.

The test facility has been designed in particular to study multi-dimensional two-phase flow effects in the upper plenum, at the upper core tie plate, and in the downcomer.

In the following, selected large scale findings from the UPTF experiments will be revealed [2]. Results of multi-dimensional flow in the downcomer, in the upper plenum, and at the upper core tie plate, as well as results related to flow phenomena in the hot and cold leg of the main coolant pipes, including studies of the reflux condenser mode, will be presented.

3. Downcomer Behaviour

Multi-dimensional flow phenomena in the downcomer have a strong impact on the overall system behaviour of a PWR during a loss-of-coolant accident. In particular the downcomer by-pass phenomena influences significantly the efficiency of the cold leg emergency core cooling (ECC) injection in case of an intermediate or large cold leg break.

To provide the required data base for full scale reactor geometry a test series of downcomer tests were performed at UPTF. Tests with steam and two-phase upflow and different ECC-injection locations were carried out to investigate downcomer behaviour during end-of-blowdown and refill phases as well as during the reflood phase of a large break loss-of-coolant accident.

Downcomer Behaviour during End-of-Blowdown and Refill Phases

During the end-of-blowdown and refill phases of a loss-of-coolant accident, steam from the core flows up the downcomer to the break. Due to flashing and entrainment in the lower plenum the downcomer upflow may be two-phase. The upflow can carry some or all of the ECC-water injected into the cold legs or the downcomer directly out of the broken cold leg and may limit or prevent the ECC-water downflow to the lower plenum.

The previous view of downcomer phenomena for cold leg ECC injection has been developed especially through the USNRC ECC By-pass Program. In this program, steam-water tests were performed at 1/30, 1/15, 2/15 and 1/5 scale at Battelle Columbus Laboratories [3] and at Creare [4]. Steady state counter current flow limitation (CCFL) tests with steam upflow and ECC-water downflow as well as transient tests involving lower plenum flashing and two-phase upflow were carried out.
Based on these experimental data of the sub-scaled test facilities empirical flooding correlations have been developed, using two dimensional groups, a modified Wallis parameter

\[ J^*_w = \frac{\dot{M}_w}{\rho_x A_{dc}} \left( \frac{\rho_x^{1/2}}{g W (\rho_w - \rho_x)} \right)^{1/2} \]

with \( W \) as average downcomer annulus circumference, and the Kutateladze number

\[ K^*_w = \frac{\dot{M}_w}{\rho_x A_{dc}} \left( \frac{\rho_x^{1/2}}{g c (\rho_w - \rho_x)} \right)^{1/4} \]

Though the tests have been carried out with a variation of the test facility scale between 1/30 and 1/5 scale the question, to what extent these findings can be extrapolated to full scale downcomer geometry, remained unanswered.

To provide CCFL and by-pass data for full reactor geometry, tests at UPTF were carried out.

In Fig. 3 UPTF results for steam upflow of 320 kg/s and ECC-injection (subcooling 115 K) in the three intact loops are illustrated. The contour plot shows isotherms of interpolated fluid temperatures (subcooling) in the unwrapped downcomer.

The two-dimensional presentation shows strongly heterogeneous flow conditions which were not obvious from small-scale experiments. The ECC-water delivered from the cold legs 2 and 3, which are located opposite the broken loop, penetrates the downcomer without being strongly affected by the upflowing steam flow. Most of the ECC-water delivered from cold leg 1, which is located near the broken loop, however flows directly to the break, bypassing the core.

The effect of ECC-injection location on downcomer phenomena is illustrated in Fig. 4. Results with slightly subcooled ECC-injection are presented in three diagrams.

In the upper diagram the established water delivery for ECC-injection rates of about 500 kg/s into each of the three intact cold legs is shown. The steam injection in the core simulator \( (M_{sc}) \) was varied between 30 kg/s and 450 kg/s. As the hot legs were closed in these tests, the steam had to flow via the downcomer to the break. Three regions regarding water penetration can be distinguished, characterized by the steam injection rate \( M_{sc} \):

\[ M_{sc} > 300 \text{ kg/s} \]

Decreasing ECC delivery from cold legs 2 and 3 with increasing steam mass flow rate, no delivery from cold leg 1

\[ 100 \text{ kg/s} < M_{sc} < 300 \text{ kg/s} \]

Complete ECC delivery from cold legs 2 and 3, no delivery from cold leg 1
\( M_{\text{CC}} < 100 \text{ kg/s} \)

Complete ECC delivery from cold legs 2 and 3, partial delivery from cold leg 1

The diagram in the middle of Fig. 4 shows the water delivery for a ECC-injection rate of about 750 kg/s into cold leg 1 only. The data show a complete by-pass at steam injection rates above 100 kg/s and even partial bypass for the lowest investigated steam flow rate of 30 kg/s.

A different CCFL behaviour was observed by injecting ECC water into the cold legs 2 and 3. The data presented in the lower diagram of Fig. 4 indicate still partial by-pass at a steam injection rate of 100 kg/s. From the results of ECC-injection into the three cold legs 1 to 3, complete water delivery would be expected in the case of ECC-injection into two cold legs 2 and 3 at similar steam injection rates. Apparently, there is a synergistic effect in which the carry over from the cold leg 1 to the broken loop is increasing the delivery from cold legs 2 and 3 to the lower plenum.

To demonstrate the effect of scaling on downcomer CCFL the data obtained from UPTF and 1/5 scale Creare test facility for ECC-injection into three intact loops are compared in Fig. 5, using the Wallis parameter as defined in Equation 1. In order to compare data of slightly subcooled conditions from UPTF with CCFL results of Creare obtained with saturated ECC-injection, an effective steam flow (injected minus condensed steam) has been introduced.

Due to the strongly heterogeneous flow conditions in the full-scale downcomer of UPTF the water delivery curves of UPTF and Creare are significantly different. For dimensionless effective steam flow, \((J'_{\text{w,CC}})^{1/2}\), greater than 0.2, the dimensionless water downflows of UPTF are much higher than the results of Creare. Note that the UPTF data at dimensionless effective steam flows smaller than 0.2 should not be directly compared to the Creare CCFL curve considering the lower scaled ECC-injection rate of UPTF compared to the Creare experiments. Higher water delivery rates can be expected below the CCFL curve if more ECC-water is injected into cold legs 2 and 3.

The main findings with respect to downcomer behaviour during the end-of-blowdown and the refill phases of an intermediate or large cold leg break with cold leg or downcomer ECC-injection can be summarized:

- there is a significant scale effect on downcomer behaviour
- the flow conditions in the downcomer are highly heterogeneous at full scale
- this heterogeneous or multi-dimensional behaviour increases the water delivery rates at full-scale relative to previous tests at sub-scale facilities
- the CCFL correlations developed from the sub-scale tests are not applicable to full scale downcomers
- the downcomer CCFL correlations for cold leg ECC injection based on sub-scale test results underpredict the water penetration to the lower plenum at full scale
due to strong heterogeneity in a real downcomer CCFL correlations have to account for the location of the ECC injection relative to the break [5].

Downcomer Behaviour during Reflood Phase

During the reflood phase the water level in the downcomer increases and approaches the bottom of the cold leg nozzles. In case of a cold leg break, the steam generated in the core flows via intact loop towards the downcomer. In combined injection PWRs, essentially all of this steam is condensed by hot leg and cold leg ECC injection and there is no steam flow into the downcomer. In cold leg ECC injection PWRs, part of the steam is condensed. The steam not condensed, along with the steam generated in the downcomer due to superheated walls, flows circumferentially around the downcomer and out of the broken cold leg, entraining and carrying away a portion of the downcomer water. The downcomer entrainment decreases the available driving head for core flooding. This contributes in combination with the steam-binding phenomena to longer quench times and potentially results in higher fuel rod cladding temperatures.

Only few data of sub-scaled test facilities are available concerning the flow behaviour in a PWR downcomer during the reflood phase. Sub-scale CCTF II test results [6] indicated no significant reduction of the downcomer water level due to water entrainment through the broken cold leg. To provide the required full scale data, reflood tests at UPTF were carried out.

During testing, manometer oscillations between the downcomer and core were observed. As the downcomer level increased, water entrainment out of the break increased. An increase in entrainment caused a pressure increase in the downcomer which forced the downcomer level down and the core level up. Consequently, as the downcomer level decreased, entrainment out of the break and the pressure in the downcomer decreased. It has been found that for a given entrainment out of the broken cold leg the water level in the downcomer approaches a state of equilibrium. The water level measurements revealed that the water level in front of the broken cold leg was higher than that at other azimuthal positions. The local increase in water level depends on the steam/water flow at the broken cold leg and average downcomer water level. A maximum increase of 0.7 m was measured.

In Fig. 6 the downcomer water level versus water entrainment through broken cold leg for different steam flows is demonstrated. As shown in this figure, water entrainment through the broken cold leg increased with increasing steam flow and downcomer level.

The main findings with respect to downcomer behaviour during the reflood phase can be summarized:

- the water entrainment out of the break is a function of steam flow and downcomer level; water entrainment increases with increasing steam flow and increasing downcomer level
- the steam flow via the intact loops into the downcomer and therefore the entrainment out of the break is reduced by steam condensation on the ECC water, the steam flow reduction is strongly affected by ECC injection rates and ECC configurations

- in a PWR with combined ECC injection (into cold leg and hot leg) the ECC flow is sufficiently high (more than 170 kg/s per injection port) to condense all the loop steam flow during reflood, therefore no substantial entrainment occurs

- In a PWR with low ECC injection rates (80 kg/s per cold leg) the reduction of the driving head of downcomer level can be of significant importance

- the sub-scale CCTF tests showed less entrainment and level reduction than comparable tests at the large scale UPTF

- the water level is higher in front of the broken cold leg than at other azimuthal positions; the magnitude of this local increase in water level depends on the steam/water flow out of the broken cold leg and average downcomer level

- all ECC water injected through the nozzle near the broken loop is entrained directly out of the break, even at low downcomer levels

4. Tie Plate and Upper Plenum Behaviour

Dependent on the type of ECC injection systems, different flow phenomena occur at the tie plate and in the upper plenum of a PWR.

For PWRs with cold leg or downcomer ECC injection, countercurrent flow of steam/water upflow and saturated water downflow occurs. The water, which is entrained by the upflowing core steam flow, is either de-entrained at the tie plate, de-entrained in the upper plenum or carried over to the hot legs. The saturated water, which is de-entrained in the upper plenum, either form a pool in the upper plenum or flows countercurrently to the steam/water upflow back through the tie plate into the core.

For PWRs with ECC injection into the hot leg or the upper plenum, countercurrent flow phenomena at the tie plate involve steam/water upflow and local down flow of subcooled water.

The knowledge about tie plate and upper plenum behaviour was based in the past on results gained from small-scale test facilities. More recently elaborated experimental results and semitheoretical correlations for the vertical countercurrent flow of steam and ECC water through the upper tie plate of PWRs can be found in [7]. The tie plates were usually simulated by small perforated plates not exceeding the size of one fuel assembly. The Wallis parameter or the Kutateladze number were applied to correlate the data.

To study tie plate and upper plenum behaviour in full reactor geometry, tests at UPTF were performed. Tests with three different types of thermal-hydraulic boundary conditions were carried out (Fig. 7):
- countercurrent flow of saturated steam and water at the tie plate (Fig. 7 a), typical of PWRs with cold leg ECC injection
- countercurrent flow of steam and saturated water injected into hot legs (Fig. 7 b)
- countercurrent flow of saturated steam and water from the core and subcooled water injected into hot legs (Fig. 7 c), typical of PWRs with combined ECC injection

Countercurrent Flow of Saturated Steam and Water for uniform flow distribution

To study the countercurrent flow at the tie plate and the liquid hold up above the tie plate in case of saturated steam/water upflow a series of UPTF tests were carried out. Reactor-typical steam/water upflow was adjusted by the core simulator with controlled injection of steam and water.

In Fig. 8 data of these UPTF tests are plotted using the Kutateladze number for upflowing steam ($K_{\text{st}}$) and downflowing water ($K_{\text{w,down}}$). In addition corresponding data are presented from single fuel assembly tests performed at the Karlsruhe Calibration Test Facility [8] to determine potential scale effects. The figure clearly shows the information that countercurrent flow limitation at the tie plate occurred at the same Kutateladze numbers in the single fuel assembly test facility and in the full size facility UPTF with approximate 20 m² total cross section.

The test results indicate that:
- the steam/water upflow, the two-phase pool above the tie plate, and the water fall back through the tie plate is uniform across the vessel
- the flooding curves for both full-scale and sub-scale test facilities are similar
- the water downflow to each fuel assembly is scale-invariant
- for homogeneous flow conditions at the tie plate the flooding curve can be defined by applying the Kutateladze number as scaling parameter

Countercurrent Flow of Steam and Saturated Water injected into Hot Legs

The situation differs strongly from the one described above in that saturated ECC water is delivered to the upper plenum via the hot legs, while steam is injected through the core simulator flowing upward through the tie plate only. This boundary condition is not reactor typical, however tests with saturated water injection allow the investigation of heterogeneous flow distribution in the upper plenum and tie plate region without the influence of condensation effects.
A series of UPTF tests were carried out investigating two different configurations of ECC-injection. In Fig. 9 the results of tests with two loop injections (injection rates 2 x 100 kg/s) and single loop injection (injection rate 1 x 400 kg/s) are shown.

The main findings of the tests performed to investigate countercurrent flow of steam and saturated water injected into hot legs can be summarized:

- water breakthrough from the upper plenum to the core occurred in front of the injecting hot leg nozzles leading to heterogeneous flow conditions at the tie plate
- water downflow and steam upflow paths at the tie plate are separated
- there is no substantial time delay between start of ECC-injection and tie plate water breakthrough
- water breakthrough rate increased with decreasing core steam flow rate
- non-uniform distribution of vertical differential pressure in the upper plenum measured across the tie plate had been detected
- the water downflow is significantly higher than that of the flooding curve determined for homogeneous flow conditions at the tie plate
- the UPTF tests indicate clearly that "classical" Kutateladze-scaling can not be applied for heterogeneous flow conditions without modifications [5, 9]

Countercurrent Flow of Saturated Steam/Water Upflow and Subcooled Water Injected into Hot Legs

Compared to saturated hot leg injection the conditions for the water breakthrough at the tie plate become more favourable if highly subcooled ECC water is injected.

In Fig. 10 the results of an UPTF test with a very high water/steam ratio of the upflow rate of w/s = 4 are shown (a typical value for the reflood period of a PWR is w/s = 2). Additional results of tests, investigating the effect of the water/steam ratio of the two-phase upflow as well as the effect of transitory flow change with increasing and decreasing upflow rates are presented.

The UPTF tests have shown that:

- the ECC penetration to the core region always follows the ECC delivery to the upper plenum without substantial delay, and occurs in front of the hot legs with ECC injection
- the time-averaged water breakthrough at the tie plate is not significantly affected by intermittent water delivery to the upper plenum compared to continuous delivery
- the water breakthrough at the tie plate increases with decreasing steam flow rate
- for a given steam upflow rate the water breakthrough at the tie plate increases with decreasing water/steam ratio of the two-phase upflow

- a two-phase pool of saturated steam and water in the upper plenum at initiation of hot leg ECC injection has only a minor effect on the water breakthrough at the tie plate

- during the period of increasing core upflow rates the water downflow is higher than for decreasing upflow rates at the same steam upflow rates

- due to heterogeneous flow conditions at the tie plate, strongly dependent on scale, the "classical" Kutateladze scaling cannot be applied without modifications [5, 9].

General Conclusions related to Tie Plate and Upper Plenum Behaviour

In general the UPTF tests reveal that the tie plate CCF behaviour with hot leg ECC-injection is quite different from that without hot leg ECC-injection, even if saturated ECC-water is delivered to the upper plenum.

The classical Kutateladze type CCFL correlation can only be used to predict the tie plate water downflow rate if no ECC-water is injected into hot legs or upper plenum. Only in this case the tie plate CCFL test results elaborated in small scale test facilities can be applied to a large tie plate.

In case of hot leg ECC-injection the water downflow through the tie plate is much higher than predicted by the previous tie plate CCFL correlations which are based on small scale test data. The reason for this deviating CCF behaviour is the inhomogenous distribution of the water mass across a full size tie plate due to local ECC-water delivery to the upper plenum. These features guarantee a good core cooling with hot leg injection, because over the full range of typical reactor core outlet flowrates the injected ECC-water penetrates through the tie plate into the core without delay and leads in combination with the cold leg injection to a fast reflooding of the core.

5. Flow Behaviour in the Main Coolant Pipes

Flow Behaviour in the Main Coolant Pipes during ECC-Injection

Pressure and fluid oscillations as well as flow regime transition can occur in the main coolant pipes of a PWR during the end-of-blowdown, refill and reflood phases due to ECC-injection. These oscillations are mainly induced by direct contact condensation of steam and the injected subcooled ECC-water.

To investigate the flow behaviour in horizontal pipes with cold leg ECC-injection via a side tube, smallscale tests ranging from 1/20 to 1/3 scale were performed previously [10]. The experiments indicated that water plug formation and oscillations may occur.
To investigate the flow behaviour in horizontal pipes with hot leg ECC-injection via an axial injection nozzle, small scale tests with models at 1/5 and 1/10 scale were carried out in the past [11].

The tests indicated that complete ECC-water reversal can occur at high steam flowrates from the upper plenum to the hot leg.

UPTF tests were carried out to investigate loop flow patterns at full-scale and to quantify the thermal-hydraulic boundary conditions which leads to pressure and flow oscillations in the loop when ECC-water is injected into the cold leg or into the hot leg [12].

Three different flow patterns were identified:

- **stable water plug** refers to formation of a quasi-steady state water plug in the pipe adjacent to the ECC injection port

- **unstable plug** implies occurrence of an unstable water plug with large oscillation amplitudes in the pipe accompanied with water hammer events

- **stratified flow** stands for establishing of water flow at the bottom and steam flow at the top of the pipe, where temperature stratification can occur in the water flow.

The UPTF test data gained at different values of pressure and ECC subcooling are plotted in diagrams (Fig. 11 and 12) using the actual steam flow rate and the steam condensation potential of the ECC water. Consequently, the maximum steam condensation potential of the ECC water (thermodynamic ratio $R_T = \frac{M_{\text{sat}}}{M_{\text{ECC}}} = 1$) is represented in these diagrams by a straight line indicating the interface between stratified flow and plug flow ranges.

For steam flows higher than the steam condensation potential of the ECC water ($R_T < 1$), stable stratified flow occurs because there has to be a flow path for the nearly saturated water at the bottom and the surplus steam at the top of the pipe. Stable stratified flow in the cold leg also occurs at steam flow rates slightly below the curve $R_T = 1$ (Fig. 11). The temperature stratification of the water flowing at the bottom of the pipe allows stable stratified flow to occur for $R_T > 1$. The extent of this region depends on the turbulence of the injected ECC water.

Fig. 11 and 12 reveal that stable water plug occurs only when the steam mass flow exceeds a certain threshold value. This threshold value is a function of the absolute pressure and also a function of the steam condensation potential of the ECC water in case of countercurrent flow in the hot leg. In this case the water plug formation in the hot leg pipe is linked to complete flow reversal of the injected ECC water.

When the actual steam flow is lower than the threshold value, i.e. the condensation potential of the ECC water is sufficiently higher than the actual steam flow, unstable plug flow with large oscillation amplitudes occurs in the cold leg or in the hot leg pipe. The steam
flow condensing on the subcooled ECC oscillates strongly, while the water plug is expelled to the downcomer or upper plenum respectively. The intermittent formation of a new water plug can give rise to water hammer loads on the pipe walls.

At low steam flow and ECC injection rates, stable stratified flow can occur up to the vertical dot-dash line (drawn in Fig. 11 and 12) which marks the minimum condensation potential of the ECC water where the steam momentum flux is sufficiently high to form a water plug.

The UPTF tests reveal that:

- plug flow occurs when the condensation potential of the ECC-water exceeds the steam flow, typical for accumulator injection
- the flow is stratified when the condensation potential of the ECC-water is less than the steam flow
- plug flow results in intermittent ECC delivery into the downcomer or upper plenum respectively, while stratified flow causes continuous ECC delivery

Flow Conditions in Hot Leg during Reflux Condenser Mode

In the reflux condenser mode heat is transferred from the core to the secondary side of the steam generators by evaporation of water in the core and subsequent condensation of that steam in the U-tubes of the steam generators. A portion of this condensate flows counter-currently to the steam through the hot leg via the upper plenum back into the core. By momentum exchange between the upflowing steam and the downflowing water in the hot legs flooding may occur, which could prevent or at least deteriorate the water back flow to the core.

Countercurrent flow in PWR hot legs has been investigated at sub-scale facilities with pipe diameters up to 200 mm (see Fig. 13).

To provide CCFL data for full-size reactor geometry, UPTF tests were performed. The results are plotted in Fig. 14 using the Wallis parameter J'. The data show that water runback to the test vessel decreases as the steam flow increases. At high steam flows (J' > 0.5), there was complete turn-around of the water flow. The close agreement of the data at the two pressures indicates that the Wallis parameter adequately accounts for pressure effects.

In Fig. 14 the UPTF tests are also compared to CCFL correlations derived from sub-scale experiments. The Krolewski correlation [13] underpredicts UPTF water runback, on the other hand the Chunki correlation [14] overpredicts runback. The Richter correlation [15], however, passes through the UPTF data, which is obviously due to the similar configuration of the flow channel.

The UPTF test No. 11 demonstrated that:
- a substantial margin exists between the flooding limit and the typical conditions expected in a PWR during reflux condenser mode of a small leak loss-of-coolant accident

6. Application of UPTF Test Results for Code Development and Code Assessment

The UPTF tests extended remarkably the experimental data base required to develop and validate analytical models used in the large thermal-hydraulic system codes for the description of phenomena in full scale reactor geometry.

To illustrate the application of the UPTF test results for model validation, the analysis of the UPTF test No. 11 [16], one of the essential tests in the ATHLET validation matrix due to the full scale reactor geometry of UPTF, will be shown.

In Fig. 15 the countercurrent flow situation during reflux condenser mode is characterized. Also the nodalization scheme of the hot leg, used in the analysis, is indicated.

In Fig. 16 the calculated CCFL mass flow rates together with the measured data are plotted in a Wallis diagram, using in contrast to Fig. 14 the cross-section of the hot leg outside the Hutze region as a characteristic dimension. Each symbol in the plot represents either one calculation or one test run.

Based on the measurement of the 3-beam-gamma densitometer, which is mounted in the hot leg at a position 6 m away from the pressure vessel, a water level height has been determined and compared with the ATHLET results.

Fig. 17 illustrates the calculated water level height along the hot leg at test conditions characterized by 15 bar system pressure and 24 kg/s vapour flow from the upper plenum to the steam generator. In this figure the Hutze region is indicated by an unshaded section. The calculated water level height can be compared with the value derived on the basis of the 3-beam-gamma-desitometer measurement. The analysis indicates that the CCFL in the hot leg occurs at the bend part of the hot leg. The accumulation of water reaches its maximum there. Consequently, the largest differences in phasic velocities are predicted for this position.

In Fig. 18 the water level heights at the position at the 3-beam-gamma-densitometer are plotted as function of the steam flow. It can be noticed that as far as the steam flow is less than the countercurrent flow limit the water level height is kept on a low level of about 0.12 m. If the steam mass flow rate reaches the CCFL conditions the water level steeply increases to the highest level height of about 0.30 m. With further increase of the steam mass flow rate the water level height decreases gradually due to the start of entrainment from the water surface. At even higher steam flow the conditions of complete flooding are reached, the water downflow to the upper plenum is completely inhibited. In this situation a large amount of water still remains in the hot leg. The water level height at the measurement position is about 0.20 m.
The water level height, derived from the 3-beam-densitometer measurement, and the calculated data are in good agreement. The typical flow behaviour at the onset of flooding is predicted by the flow model implemented in ATHLET.

The UPTF test No. 11 confirms that:

- the flow model of ATHLET basing on Wallis parameter for steam and water, which has been validated previously on small-scale tests, predicts the countercurrent flow limitations also in case of a large diameter pipe of 0.75 m.

7. Conclusions

To investigate two-phase flow phenomena occurring in PWR primary systems during loss-of-coolant accidents with active emergency core cooling systems, a series of tests have been performed at the 1:1 scale test facility UPTF.

The UPTF tests have remarkably extended the data base required to develop and validate analytical models used in the large thermal-hydraulic codes for the simulation of two-phase flow phenomena in full scale reactor geometry.

Based on comparisons with test results from sub-scale facilities the following conclusions can be drawn with respect to the scalability of the two-phase flow phenomena:

- for two-phase flow conditions in horizontal and inclined pipes (1-D components) the classical J' scaling can be applied successfully

- for inhomogeneous flow, e.g. in the upper plenum or downcomer, there is a need for improved multi-dimensional modelling

Acknowledgement

The strong support by the German Federal Minister for Research and Technology for the 2D/3D program within which the UPTF experiments were performed, is highly acknowledged.
NOMENCLATURE

A  cross section area, \( m^2 \)
D  diameter, \( m \)
F  condensation efficiency, [-]
g acceleration due to gravity, \( m/s^2 \)
h  height, \( m \)

\[ K^* = \frac{10^2}{\rho} \cdot \frac{g}{\sigma} \cdot \Delta \rho^{-1/2} \] Kutateladze number, [-]

\[ J^* = \frac{10^2}{\rho} \cdot \frac{g}{D} \cdot \Delta \rho^{-1/2} \] Wallis parameter for downcomer, [-]

\[ J^* = \frac{10^2}{\rho} \cdot \frac{g}{W} \cdot \Delta \rho^{-1/2} \] Wallis parameter for downcomer, [-]

M  mass flow, \( kg/s \)
M_{steam}  steam condensation potential of ECC, \( kg/s \)

P  absolute pressure, \( bar \)
R_t  thermodynamic ratio, [-]

T  temperature, \( K \)
W  average downcomer annulus circumference, \( m \)

\( \Delta \rho = \rho_w - \rho_s \)  difference of densities, \( kg/m^3 \)
\( \rho \)  density, \( kg/m^3 \)
\( \sigma \)  surface tension, \( N/m \)

Subscripts

C  core
cond condensation
d  down; delivery
DC  downcomer
eff effective
H  hydraulic
s  steam
w  water
x  water or steam

ABBREVIATIONS, ACRONYMS

BCL  broken cold leg
CCF(L)  countercurrent flow (limitation)
CL  cold leg
ECC  emergency core cooling (coolant)
MCL  main coolant line
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Upper Plenum Test Facility-Primary System

Fig. 1 Upper Plenum Test Facility (UPTF) Primary System
UPTF-Test Vessel and its Internals

Fig. 2  Test Vessel of UPTF with Core Simulator, Downcomer, and Upper Plenum
ECC injection ($\Delta T_{sub} = 115$K, $p = 5.3$ bar)

511 into loop 1

504 504 kg/s

3 2

MCL

Downcomer height (mm)

90° 360° 270° 180°

320 kg/s
Steam mass flow

Fig. 3 Countercurrent flow conditions in full-scale downcomer for strongly subcooled ECC, distribution of subcooling
Fig. 4  Effect of loop arrangement on water delivery to lower plenum for nearly saturated ECC in countercurrent flow, UPTF tests 6, 7 and Z3B
Comparison UPTF - Creare

\[
(J^*_{s, et})^{1/2} = (J^*_{s,C} - FJ^*_{s, cond})^{1/2}
\]

Fig. 5 Effect of geometrical scaling on water delivery to lower plenum for nearly saturated ECC in countercurrent flow, UPTF tests 6, 7 and Z3B
Fig. 6 Water entrainment through broken cold leg vs. downcomer for different water levels, steam flow and ECC injection rates

Water entrainment through broken cold leg, $M_w$ in kg/s

<table>
<thead>
<tr>
<th>Broken cold leg steam flow kg/s</th>
<th>Cold leg ECC injection, kg/s</th>
</tr>
</thead>
<tbody>
<tr>
<td>240</td>
<td>(3 x 80)</td>
</tr>
<tr>
<td>336</td>
<td>(3 x 112)</td>
</tr>
<tr>
<td>420</td>
<td>(3 x 140)</td>
</tr>
</tbody>
</table>

42 - 44
31 - 35
26 - 30
18 - 24
13
a. CCF of saturated steam and water with homogeneous water distribution at tie plate

b. CCF of steam from the core and saturated water from the hot leg with heterogeneous water distribution

c. CCF of saturated steam and water from the core and subcooled water from the hot leg with heterogeneous water distribution

Fig. 7 Countercurrent flow conditions at the tie plate addressed in UPTF tests
Fig. 8  Countercurrent flow of saturated steam and water at the tie plate
$M_W = 2\times100 \text{ kg/s}$  
$M_W = 400 \text{ kg/s}$

HL1  
HL2  
HL4 Break  
HL3  

HL1  
HL2  
HL4 Break  
HL3

240  
160  
120  
80  
40  
0  
200  
300  
400  

Core steam flow $M_{SC}$

Water downflow $M_{W, down}$

ECC Injection
$M_W = 2\times100 \text{ kg/s}$  
$+M_W = 1\times400 \text{ kg/s}$

CCFL Correlation (s. Fig. 8)

Fig. 9  Countercurrent flow of steam and saturated water injected into hot leg
Fig. 10 Countercurrent flow of two-phase up-flow and subcooled water downflow during hot leg ECC injection
Fig. 11 Flow patterns for cocurrent flow in the cold leg
Fig. 12 Flow patterns for countercurrent flow in the hot leg.
Ohnuki  
D = 26/51/76 mm  
Incination angle  
\( \alpha = 45^\circ \)

Krolewski  
D₁ = 50.8 mm  
L/D₁ = 11.5  
D₂ = 102 mm  
\( \alpha = 90^\circ \)

Richter et al.  
D = 203 mm  
\( \alpha = 45^\circ \)

UPTF  
D = 750 mm  
\( \alpha = 50^\circ \)

Fig. 13 Countercurrent flow studies in PWR hot legs
Fig. 14 UPTF test data compared to correlations derived from subscale tests
Fig. 15: Schematic View of Counter-current Flow and Nodalization of the Hot Leg

D = 0.75m
Flow Area = 0.4418m$^2$; hydr. Diam. = 0.75m

Fig. 16 UPTF and ATHLET data plotted in a Wallis diagram
Fig. 17 Water level heights along the UPTF hot leg
Fig. 18 Dimensionless water level height at 15 bar
The Contribution of UPTF Experiments to Resolve Some Scale-Up Uncertainties in Countercurrent Two Phase Flow

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ABSTRACT

The volume scaling of previous existing integral test facilities is up to a maximum of 1/48 of the full size power reactors (or 1/25 for the reflood integral test facility CCTF). The experimental results achieved in these facilities or in down-scaled separate effects test facilities have to be extrapolated to reactor scale in order to evaluate the full size reactor thermal hydraulic behaviour. There is some uncertainty in extrapolating the small scale results.

Experimental results from the 1:1 scale UPTF test facility can considerably reduce the uncertainty of geometrical scaling. The final goal is to qualify the overall validity of computer program simulation, and to quantify the uncertainty of the computer program calculations for full size reactors.

Significant scaling dependent experimental results have been found in UPTF separate effects tests in the case of heterogeneous steam-water flow conditions. This is especially the case for vertical and horizontal countercurrent flow.

1 INTRODUCTION

The analytical simulation of transient two-phase flows is an essential element to understand many safety-related problems of the operation of nuclear power plants. Several computation codes have been developed during the past two decades and a large number of code versions exists which has been subject to extensive validation activities. Integral experiments as well as separate effects tests serve as background to compare calculated with measured parameters and to draw conclusions on the adequateness of the computation methods. In the early stage of this development the codes were primarily used to de-
sign the engineered safety systems (e.g., emergency core cooling systems, ECCS). During the development period of light water reactor technology very stringent rules, well known as "Appendix K requirements"/1/ were issued which had to be observed in code application to warrant a conservatism in the design of the safety systems and to compensate for the limited overall knowledge in this field.

Nowadays, two main incentives exist to improve our knowledge about the achievable accuracy of two-phase flow codes when and where applied in a predictive manner. Firstly, there is the desire to replace the conservative requirements for code application within licensing procedures by a "best-estimate" concept in combination with some safety factors to compensate for proven predictive inaccuracies of the codes. Secondly, modern transient two-phase flow codes form an essential basis for the elaboration of accident management procedures to mitigate abnormal beyond design basis accident (DBA) plant conditions. For this purpose codes must be utilized in a "best-estimate" manner (e.g., within plant analyzers) to allow the permanent comparison to the plant status on basis of a few measured parameters. However large uncertainties exist with respect to predictive code accuracy in particular for the beyond-DBA application.

Numerous code validation activities were performed in the past. A certain stabilisation of the State-of-the-Art can be noticed, which indicates that several shortcomings of code application have not been resolved during the last ten years of advanced code development. Important questions remained unanswered in particular for the predictive code application to full-size power plants:

1. May we anticipate that the set of conservation equations and constitutive equations processed by thermal-hydraulic codes is suitable for all test rig dimensions?

2. If this is not the case, which methods are available to assess the uncertainty for code application for full size nuclear power plants?

3. How large is the expected uncertainty band of predictive code calculations and to which extent could this be caused by the limited range of applicability of the processed equations?

4. Space discretisation or nodalisation has been identified as a highly empiric element of code application for transient two-phase flow predictions. This may be also true for full-size plant application for which nearly no comparison to measured data is possible. This leads to one of the main questions: What is a useful nodalisation scheme for a nuclear power reactor simulation?

The earlier code validation methodology was based on the comparison of code calculated results with measured parameters of a large number of isolated small scale reactor safety experiments. Only during the last few years, experimental results from large scale test facilities became available, confirming the possibility that many earlier code validation findings may be strongly biased towards the facility dimensions of mainly SEMISCALE and LOBI.

"Scaling" in general encompasses all differences existing between a real full-size industrial plant (e.g., a nuclear power plant system) and a corresponding experimental facility. An experiment may differ in geometric dimensions and shape, in arrangements and availability
of components, in the mode of operations (e.g. nuclear vs. electrical heating) or even in
the applied test fluid (e.g. Freon vs. water).

All these differences may distort experimental observations precluding direct application to
the design or operation of full-size plants. An extrapolation procedure is necessary which
requires good understanding of the experiment and the involved processes to the largest
possible extent.

"Distortions" are defined as partial or total suppression of physical processes or phenom-
ena caused by differences in geometric dimensions or the arrangement of components of
the test rigs. Other phenomena (e.g. heat losses) may dominate small scale facility be-
haviour but become less importance for large scale system tests. Distortions may also be
caused by differences in the mode of operation of the test rigs or by the use of different
fluids or different hardware materials if compared to the full-size plant.

Geometric similarity between experimental test rigs and the nuclear power plant the loop
systems has been abandoned in favour of a preservation of geometric elevations, which is
a decisive parameter for gravity dominated natural circulation processes. Thus, in many
test facilities the reduction of the primary system subvolumes was largely achieved by an
equivalent reduction in vertical flow cross sections. Under the conditions of single phase
forced or natural circulation it was principally possible to minimize flow distortions in the
scale test rig by a proper combination of the hardware or component elevations with addi-
tional orificing at selected locations within the integral loops. This holds also for those
periods of strong two-phase flow transients which are characterized by quasi-homoge-
nous flow patterns (e.g. the initial blowdown period of a large break loss of coolant acci-
dent - LBLOCA). Similarity of transient fluid flow properties might be expected for such
situations. However, for most small break LOCA natural circulation periods or other two-
phase flow conditions which are dominated by slow pressure transients and pronounced
phase separation processes, the drastic reduction of the horizontal or vertical flow cross
sections may have an impact on the observed local flow patterns. Under these circum-
stances distortions are inevitable and the experimental observations, in particular under
transient conditions, will be scale or test rig dependent.

2 INTEGRAL TEST FACILITIES

A large number of thermal hydraulic test facilities have been designed and operated pro-
viding a huge data base for code validation purposes. Separate effects test (SET) facilities
have been designed for the study of particular phenomena like two phase flow stratifica-
tion, water level swell, critical two-phase flow and heat transfer processes between fuel rod
surfaces and mono- or biphasic coolant. In general, the experimental boundary conditions
under which separate effects tests have been operated have been specified from observa-
tions obtained from integral test facilities. Mostly, SET-tests can be considered as basic ex-
periments covering wide parameter fields providing the data base for code validation for
specific phenomena.
Integral test facilities are of particular importance for the research into the interaction of important reactor components (e.g. the core, the steam generators, the pumps and the recirculation line systems) involving in an integral manner all the important transient two phase flow phenomena which might govern the accident behaviour of full size commercial nuclear power plants. In this respect the experimental evidence from integral test facilities has a dominant position for a credible code validation process. Both, separate effects test rigs and integral test facilities complement each other in providing the data base. However, priority consideration has been given to the code validation based on integral test facilities. This may be best characterized by the tests addressed within the first CSNI code validation matrix of thermal hydraulic codes for LWR-LOCA and transients issued more than 5 years ago [2/]. Focusing particularly on experiments from integral test facilities available at that time most of the data offered for code validation have been obtained from small scale test facilities. Subsequently, most code validation work was indeed based on these tests. Some work is presently ongoing to generate a complementary code validation matrix for specific phenomena studied in separate effects tests.

11 integral test facilities can be identified. Some of them are still existing, many of them have been dismantled and are not anymore available. Tables 1 and 2 provide an overview on these test facilities indicating the country of location, the owner, the achievable thermal power and the important scaling ratio "primary system volume of a prototype PWR to primary system volume of the test rig". The volumetric scaling ratio contains the basic information identifying the size of the test rig. Only the magnitudes of this ratio are of interest because the typical dimensions of a prototype PWR may differ from case to case. More details concerning detailed geometric information of these test facilities can be found in [3/].

Certain phenomena dominating transient two phase flow processes may be governed not only by the volume ratios but also by other reduced geometric dimensions. E.g. is natural convection strongly influenced by a preservation of geodetic elevations. Phase separation in horizontal lines on the other hand may be dominated by the available flow cross sections. Fig. 1 provides some illustration of ratios of existing particular hardware dimensions for several test rigs listed in tables 1 and 2. As evident from Fig. 1 most test facilities were designed to preserve geodetic elevations. Combining volumetric scaling with the preservation of geodetic elevations resulted in the well-known distortion of the visible shape of test facilities as illustrated by Fig. 2.

Early in time, it was identified that specific phenomena like stratification, water entrainment and vapour carryunder are obviously not scale independent. Distortions of observed flow phenomena were to be expected which have eventually been processed into empirical correlations derived from such scale experiments. Water carry-over between the ECC injection location and a large leak (typical for a DBA situation) was the most prominent parameter suspect to be dependent on test rig dimensions. Relevant hardware dimensions involved were the downcomer annulus gap and the associated dimensions of recirculation lines and the pressure vessel dimensions itself [4, 5/].

Another scale-dependent process was the penetration of ECC water injected via the hot legs into the core region of KWU-designed pressurized water reactors. Experimental evidence obtained from the 1:135 scaled PKL facility indicated the urgent need to study scal-
ing issues in this context. As a consequence the Federal Republic of Germany decided to
design, build and operate the full scale UPTF facility [5].

3 OVERVIEW ON UPTF EXPERIMENTAL GOALS

The UPTF Experimental Program sponsored by the Ministry for Research and Technology
(BMFT) is the German contribution to the trilateral 2D/3D project, and is performed within
international cooperation among Japan (JAERI), USA (USNRC) and the Federal Republic
of Germany (BMFT). UPTF is a full scale test facility of a four-loop 1300 MW(e) pressurized
water reactor including the reactor vessel (with downcomer, lower plenum, core simulator,
upper plenum) and four loops (with pump simulators and steam generator simulators).
Major dimensions of the UPTF are depicted in Fig. 3.

The upper plenum including the internals (columns), the downcomer and the four con-
ected loops are represented in 1:1 scale. The maximum pressure is limited to 2 MPa. The
steam produced in a real core and the water entrained by this steam flow are simulated by
controlled steam and water injection through the core simulator nozzles. 193 steam/water
injection nozzles are installed directly below the 193 dummy fuel assemblies, each consist-
ing of a 16 x 16 rod array (0.775 m long) with the upper two grid spacers and the upper
end box including the upper tie plate.

The three intact loops are equipped with flow restrictors to simulate the reactor coolant
pumps, and with steam /water separators representing the steam generators. The hot and
cold legs of the broken loop lead through steam/water separators and break valves into
the containment simulator. Breaks of variable size can be simulated in both the hot and the
cold leg. Detailed information on UPTF system characteristics is given in [7].

The objective of the test program is the full-scale investigation of the three-dimensional
single- and two-phase flow behaviour in the primary system of a PWR during the end-of-
blowdown, refill and reflood phases of a loss of coolant accident.

The experimental program involves the simulation of various types of PWR's with different
ECC systems including cold leg injection, hot leg injection, simultaneous hot and cold leg
injection and downcomer injection. The influence of vent valves on core cooling are also
investigated.

Separate effect and integral tests were performed to study thermal hydraulic phenomena
in the upper plenum, across the upper core tie plate, in the downcomer and in the hot and
cold legs of the primary system. Three basic ECC water injection modes were investigated
in UPTF:

- cold leg injection delivering ECC-water via the cold leg, the downcomer and the lower
  plenum into the core,
- hot leg injection delivering ECC-water via the hot leg, the upper plenum and through
  the upper tie plate into the core,
- combined injection coupling both cold and hot leg injection.

In addition, tests concerning small break LOCA problems were performed at lower pressures (smaller than 2 MPa) to investigate

- fluid/liquid mixing phenomena in the cold leg and the downcomer
- countercurrent flow phenomena in the hot leg under reflux condenser conditions.

4 ISSUES RESOLVED BY UPTF DATA

An overview on the UPTF data is given in /8/. The emphasis of the present paper is to derive scaling laws based on UPTF 1:1 scale separate effects test results for downcomer and upper tie plate countercurrent flow and to compare multidimensional vertical flow with countercurrent homogeneous vertical as well as heterogeneous horizontal and inclined countercurrent flow conditions. Physical reasons will be presented for different scaling behaviour in different geometries and for different gas-liquid distributions. The objective is to reduce the uncertainty of geometrical scaling. The present investigation is a basis to qualify the overall validity of computer program simulation, and to quantify the uncertainty of computer program calculations for full size reactors.

Data from the UPTF program were used in evaluating phenomena which are important for refill and reflood during large break LOCA:

- Countercurrent flow in the downcomer (delivery) and/or bypass of ECC water (out the break)
- countercurrent flow through the upper tie plate (delivery to the core and/or carryover out the break of ECC water injected into the hot legs or directly into the upper plenum)
- liquid-vapour mixing with condensation (subcooled ECC water injection into cold and hot legs, condensation in cold leg, downcomer as well as hot leg and upper plenum; influence on countercurrent flow in downcomer and upper tie plate)
- entrainment and deentrainment in the upper plenum and hot legs in the case of cold leg ECC water injection (important for steam binding which is detrimental to core cooling).

UPTF data within the 2D/3D project which are important for small break LOCA were obtained for

- countercurrent flow in the hot leg during reflux-condenser conditions.

In order to reveal scaling dependent phenomena UPTF results are compared with results gained from small scale test facilities. Proposals are made to quantify the scaling dependencies. A new flooding correlation for the limitation of steam-water countercurrent flow is proposed in order to describe data up to reactor scale downcomer and upper tie plate.
4.1 Vertical Asymmetric Heterogeneous Countercurrent Flow in the Downcomer and Upper Tie Plate

Objective of the downcomer and upper tie plate countercurrent flow tests in UPTF is to investigate if and how much ECC water is flowing downward in the downcomer and into the lower plenum as well as downward through the upper tie plate if steam generated in the core region is flowing to the broken cold leg and to the broken hot leg as shown in Fig. 4. Water downflow in UPTF was observed below those legs where ECC water was injected. The main water downflow in UPTF is below those legs which are furthest from the broken legs.

The cold leg arrangement is shown in Fig. 5. Fig. 6, taken from /9/, shows the flow conditions in the downcomer projected into a plane, where the distribution of the measured subcooling pertains to a test where 400 kg/s steam is injected, and 3 x 500 kg/s slightly subcooled ECC water are injected. The measured subcooled regions indicate water flow whereas saturation temperature indicates steam/droplet flow. The cold leg connections are represented by white circles including the cold leg number. There is no water downflow below cold leg 1, which is the closest to the broken cold leg. Water is partly flowing down below cold legs 2 and 3, partly collecting in the upper part of the downcomer and partly flowing to the broken cold leg. Water downflow below cold leg 1 is observed only if the steam mass flow rate from the core region is lower than 100 kg/s (for the pressure range used in the experiments).

If ECC water is injected into the hot legs it may flow down through the upper plenum and the upper tie plate into the core region. The water downflow location in the upper plenum determines the downflow location through the upper tie plate. Fig. 7 shows the hot leg and the guide tube column arrangements in the UPTF upper plenum. Fig. 8 gives an impression of the flow conditions at the upper tie plate for a test where 3 x 400 kg/s subcooled ECC water (injection temperature 30°C) and 185 kg/s steam from the core region are injected. The subcooling measured 10 mm below the tie plate is shown. The subcooled regions are representing the ECC water downflow locations. Drag-body and break-through-detector measurements in the tie plate show the same distribution of water downflow and steam/water upflow. The water downflow area fraction of the tie plate flow cross section has been determined for this test to be approximately 32%. Steam and droplets are flowing upwards in the remaining part of the tie plate flow cross section.

The pronounced stable separated regions of water downflow and steam upflow which are typical for vertical asymmetric heterogeneous countercurrent flow in the UPTF downcomer and upper tie plate regions could not be observed in small scale facilities. Heterogeneous distribution of gas upflow and water downflow is also developing in small scale facilities but the locations of gas and liquid flows are alternating.

4.1.1 Saturated ECC Water

UPTF results indicate that the larger the size of a flow passage (such as the reactor vessel downcomer, or the core tie plate), combined with nonuniform geometric distribution of
water inlet and flow outlet, the more multidimensional the flow field becomes. Fluids of different density and different direction of flow find it much easier to establish their own preferred flow paths in which the flow is predominately co-current rather than countercurrent. For a prediction of the performance of the Nuclear Reactor ECC Systems it is important to be able to calculate the water downflow rate under certain upflow conditions for the total downcomer and tie plate regions.

Correlations to describe the limitation of steam-water countercurrent flow, called flooding, are based on saturated water flow. The additional effects of condensation and eventually water entrainment in the upflow from below the limiting location for countercurrent flow can be included in the upward momentum term. This will be described later.

Two classic flooding equations are available to describe the limitation of vertical steam-water countercurrent flow. These are the well-known Wallis-type and Kutateladze-type correlations. The first one describes flooding in small scale flow channels /10/, see Table 3 (J*-and J*-correlations). The constants in the correlations are determined by small scale experiments /10, 11/. If we look at one extreme case, namely zero liquid downwards flow, the Wallis flooding correlation suggests an increase of gas momentum with scale of the flow channel. However, the flooding process is always coupled with instabilities of the gas/liquid interface, Fig. 9. These instabilities may restrict the upwards gas velocity which would allow downwards penetration. If a critical wavelength is exceeded the wave becomes unstable. The unstable growing waves are disintegrated by the gas flow and the droplets are carried with the gas upflow, limiting the liquid downflow. This is a very important mechanism of the flooding phenomenon. The effect of instability of the gas-liquid interface is included in the Kutateladze-type flooding correlation /12/, see Table 3 (K-correlations). This equation can be derived from a force balance perpendicular to the main steam and water flow directions /16/. The constants are determined by small scale experiments /13, 14/.

In order to describe asymmetric heterogeneous gas/liquid countercurrent flow in the total reactor scale downcomer and tie plate region it is necessary to correlate local steam velocities of the multi-dimensional flow field with the superficial steam velocity. Therefore, the Kutateladze-type flooding equation existing hitherto has to be extended. The flow is asymmetric if the ECC water is injected via the hot or cold legs and the steam/water mixture is flowing out of one broken hot or cold leg. In UPTF downflow was observed near some of the injecting legs when water downflow occurred through the downcomer or the tie plate. A new correlation has been developed by Glaeser /15, 16/ in order to describe the asymmetric heterogeneous flooding behaviour observed in the UPTF facility, see Table 4. The correlation is described in /15/ and more details of the derivation and application of the correlation can be found in /16/. A dimensionless geometrical lateral distance between the legs with ECC injection and the broken leg is introduced in the gas upflow momentum term. This term relates the local upward gas velocity at the water downflow locations to the superficial gas velocity. The superficial gas velocity can be calculated from the steam mass flow rate. For low values of the geometric distance L the new correlation turns into the Kutateladze-type correlation, see Tables 3 and 4.

If there is more than one ECC injection location, the arithmetic mean value of all distances L between the ECC injection legs and the broken leg has to be used in the correlation. However, only those injection locations can be considered where water can flow downward. This means that the modified dimensionless gas velocity obtained by using the value
of L for the individual injection location has to be below the onset of penetration point, see Table 5. Otherwise, the respective ECC injection leg cannot be included in the arithmetic mean value L. The experimental data can be very well described by this correlation /15, 16/.

The resulting lowest gas velocity for zero water penetration (onset of penetration) is shown in Fig. 10 compared with the downcomer circumference scale. Water downflow is impossible for gas velocities above the curve. There are three different scaling regions on which one of the already described respective flooding correlations is applicable (Tables 3 and 4). These are the classic Wallis- and Kutateladze-type as well as the new correlation. The range of applicability is dependent on the dimensionless annulus circumference, which governs the different flooding correlations. It can be seen from Fig. 10 that it is impossible to extrapolate countercurrent flow correlations from small scale data below one-ninth downcomer circumference scale (equivalent to 1/81 flow cross section scale) at a pressure $p = 400$ kPa, for instance, to reactor scale. The large scale UPTF data were needed to clarify the influence of scaling on the ECC flooding phenomenon. The new correlation has been checked for pressures between 300 and 1200 kPa.

The applicability ranges of the three flooding correlations for the upper tie plate region are given in Fig. 11. The dependence of the lowest gas velocity for zero water penetration (onset of penetration) upon the equivalent diameter scale is similar to that of the downcomer. The flooding correlations for the three scaling regions are listed in Tables 3 and 4. These are again the classic Wallis- and Kutateladze-type as well as the new correlation using an appropriate geometrical parameter. For the tie plate, it is impossible to extrapolate from small scale data below one-third linear scale (at $p = 400$ kPa) which is equivalent to one-ninth flow cross section scale up to reactor scale. The new correlation has been checked for pressures between 400 and 800 kPa.

It is obvious from Figures 10 and 11 that UPTF countercurrent flow limitation data are above the small scale data. This means that a water downflow is possible at higher superficial steam velocities in UPTF than in small scale facilities. The superficial steam velocity for which countercurrent flow is possible also increases with the increasing distance between ECC injection leg and broken leg in UPTF, i.e. from ECC water injection in a leg close to the broken leg up to ECC water injection opposite to the broken leg.

4.1.2 Subcooled ECC Water

The ECC systems in nuclear reactors are injecting subcooled water. Considering flooding in combination with subcooled water injection is therefore important for nuclear reactor safety. Flooding in the case of subcooled ECC injection is also scaling dependent as in the case of saturated water injection. The steam upflow velocity, however, determining the water downflow rate may be reduced due to condensation. This effect can be well described based on the flooding correlations developed for saturated water injection.

If subcooled ECC water is injected into the cold or hot legs and is flowing from there into the upper part of the downcomer or the upper plenum, respectively, the gas upflow superficial velocity in the flooding correlation has to be reduced by the condensation rate. This
leads to a higher liquid downflow rate at the same steam flow rate from the core region, however, reduced steam flow in the flooding flow cross section compared with downflowing saturated water, Fig. 12. The calculation procedure is well known and can also be applied to the new flooding correlation which is applicable to vertical asymmetric heterogeneous countercurrent flow (16), Table 6.

No effect of subcooled water injection on the flooding behaviour was observed when the upwards steam flow rate was higher than the onset of penetration rate and when the ECC water was injected via the cold and hot legs of the main coolant loops. However, after the onset of penetration condensation has to be considered. In order to determine the condensation superficial velocity \( j_{\text{cond}} \), the condensation efficiency \( f \) has to be determined from experimental data. The UPTF experiments show that the condensation efficiency is dependent on the fraction of the injected ECC water flowing downward through the downcomer or downward through the upper plenum and core region. Since water downflow is dependent on the steam flow rate, some iterations are necessary for calculating the water downflow rate for a given steam flow rate.

Condensation efficiency, as determined from UPTF data, can be represented by

\[
f = 0.87 \frac{\dot{M}_i \downarrow}{\dot{M}_{\text{ECC}}}
\]

The maximum condensation efficiency (0.87) applied to reduce the steam superficial velocity in the gas upflow momentum term in the flooding correlation agrees with the measured condensation efficiency for the region including cold leg where water is injected, total downcomer and lower plenum. The fact that the condensation is high in the upper downcomer region, and that therefore the steam velocity is higher in the lower part of the downcomer than in the upper part, does not appear relevant for calculating the condensation effect in the flooding correlation.

For ECC water injection into the hot legs the total condensation efficiency is 0.87. The total condensation efficiency includes the region of hot legs with ECC injection and upper plenum (\( f = 0.66 \)) and below the upper tie plate (\( f = 0.21 \)). It appears that not only the condensation below the tie plate is determining the calculated upward steam superficial velocity through the tie plate. Condensation on the downflowing water in the upper plenum as well is determining the steam velocity distribution through the tie plate, to such a degree that the total condensation efficiency in both the upper plenum and below the tie plate has to be considered. It should be noted that condensation in the upper plenum determines if the steam flow rate is sufficient to carry water from the upper plenum out to the broken hot leg.

4.1.2.1 Flooding in Downcomer

Figure 13 shows the new modified gas upflow correlation versus the liquid Kutateladze number for the downcomer. The new flooding equation developed by Glaeser (15, 16) is a good representation of all the UPTF data points obtained with saturated water injection. This curve is the flooding line for a large scale downcomer. Countercurrent flow is imposs-
A water downflow occurred below two of the three injection cold legs in UPTF test 5B (3 x 500 kg/s subcooled ECC water, injection temperature 30°C). The measured water downflow is 970 kg/s and according to the equation given above the condensation efficiency is calculated to 0.56. Applying this condensation efficiency the 

$$(K_y/L)^{1/2} \text{ value is reduced from } 0.0152 \left(\dot{M}_{\text{w},\text{in}} = 320 \text{ kg/s; } K_y^{1/2} = 3.41\right)$$

\[ \text{to } 0.0093 \left(\dot{M}_{\text{w},\text{in}} = 120 \text{ kg/s; } K_y^{1/2} = 2.1\right), \]

which results in $K_y^{1/2} = 1.38 \left(\dot{M}_{\text{w},\text{in}} = 970 \text{ kg/s}\right)$, see Fig. 13.

The calculated water downflow is in very good agreement with the measured value. The dimensionless superficial velocities are determined using the steam and water properties for the measured pressure $p = 520 \text{ kPa}$. The average distance between ECC injection legs and broken leg is calculated to $L = (L_x + L_y)/2 = 6.97 \text{ m}$.

A water downflow below cold legs 2 and 3 is possible for the core simulator steam injection rate of test 5B. A 1000 kg/s maximum ECC water downflow is possible (2 x 500 kg/s ECC water injection rate) due to a steam upflow rate reduction by condensation in the downcomer region. Due to condensation and reduction of steam upflow at the water downflow location the water downflow is increasing very rapidly up to approximately the injected water mass flow rates below these injection legs. This is called “water break through”.

Even due to condensation on subcooled water below cold legs 2 and 3 no water downflow below cold leg 1 is possible for a steam flow rate from the core of 320 kg/s. Downflow is only possible if the steam velocity is at or below the onset of penetration gas velocity given by

$$K_y^{1/2} = 2.1 \text{ or } (K_y/L)^{1/2} = 0.024, \text{ where } L_i = 1.102 \text{ m}.$$  

Consequently, the steam mass flow rate from the core region should be lower than 290 kg/s for getting an additional ECC water downflow in front of cold leg 1. The subcooled ECC water countercurrent flow behaviour in test 5B can be very well explained by the flooding curves derived from saturated water tests, taking into account the additional condensation effects.

4.1.2.2 Flooding in Upper Tie Plate

Figure 14 shows the experimental saturated water data of UPTF for the upper tie plate in terms of the new gas upflow expression versus the liquid Kutateladze number. The agreement between the correlation developed by Giaeser/15,16/ and measured values is good. If the water downflow rates are approaching the ECC injection rates, the measured values are lower than the calculated values due to entrainment in the upper plenum (limited due
to injection flow rate). Included in Figure 14 is the result of UPTF test 12 representing sub-
cooled water injection.

Water downflow through the tie plate occurred below hot legs 1, 2 and 3 in test 12 (3 x 400
kg/s subcooled ECC water injection, temperature 30°C). The downflow rate through the tie
plate was determined to be approximately in the range of 1200 kg/s to 1350 kg/s during
this part of the test. For example, the measured water downflow was $\dot{M}_w = 1200$ kg/s (ECC
water plus a fraction of steam condensed in the upper plenum), corresponding to $K_{v}^{10} =
1.54$ when the pressure was $p = 530$ kPa at the start of downflow. According to Fig. 14,
this downflow is possible only if $(K_{v}^{10})^{10} = 0.0038$. In order to meet this condition based
on the core simulator injection mass flow rate

$$\dot{M}_{sw} = 272 \text{ kg/s} (K_{v}^{10} = 3.08), \text{ a condensation mass flow rate } \dot{M}_{p,cond} = 259 \text{ kg/s} \text{ is necessary. This corresponds to a condensation efficiency of } f = 0.87 \text{ which is in good agreement}
\text{ with the measured condensation efficiency for the total pressure vessel.}$

According to the injected steam flow rate water downflow is possible below hot legs 2 and
1 in test 12. Due to condensation on the downflowing ECC water, steam upflow rate is re-
duced according to the condensation potential of the water downflow so that a maximum
water downflow of 800 kg/s (2 x 400 kg/s) below hot legs 2 and 1 is possible. The steam
velocity is already below the onset of penetration limit of hot leg 3 for this water downflow
rate. Therefore, water downflow below the hot leg 3 is also possible. Hot leg 3 is the
closest to the broken hot leg. Again, the subcooled ECC water countercurrent flow behav-
our can be very well explained by the flooding curves derived from saturated water tests,
taking into account the additional condensation effects.

4.1.3 Steam and Entrained Water Upflow from the Core Region through the Upper
Tie Plate

Since onset of ECC water penetration occurs at higher steam superficial velocities in large
scale facilities than in small scale geometries the steam may carry a higher water droplet
mass flow rate per flow cross section, i.e. a higher entrainment rate (as will be described
later). Therefore, the entrainment from below the flooding flow cross section may have a
more significant effect on the flooding behaviour in large scale than in small scale facilities.
This is a consequence of the previously described scaling effect which is observed in
UPTF.

Liquid droplets entrained by the upward steam flow from the core region, i.e. from below
the flooding flow cross section in the tie plate cause an additional upflow momentum effect
on countercurrent flow, /15, 16/, Table 6 and Fig. 15. It is not known to the authors that this
effect has been investigated in vertical countercurrent separate effects tests prior to UPTF
tests /7/ and prior to UPTF calibration tests /14/, which were performed in the Karststein fa-
cility consisting of a single fuel assembly and a single steam/water injection nozzle simulat-
ing steam and steam/water upflow through the upper tie plate.

Fig. 16 shows the measured and calculated core simulator steam mass flow rate applied
per fuel assembly for onset of water penetration versus the tie plate equivalent diameter
scale. This is a dimensional representation of Fig. 11 for saturated ECC water. Also included are UPTF experiments with subcooled ECC water injection (tests 12, 13 and 15 A). The steam mass flow rates injected in both the test 12 and the saturated water test (test 10 A) were not high enough to prevent an ECC water downflow through the tie plate. Thus, the highest measured steam flows for these two tests are lower than calculated for onset of penetration. The onset of penetration steam velocity is extrapolated from saturated water test 10 A data, as can be seen from Fig. 14. Two tests were performed with a steam/water upflow through the tie plate simulating water entrainment from the core region (tests 13 and 15 A). Test 13 was performed with a water to steam mass flow ratio of 4 via the core simulator injection nozzles and the mixture injection rate was continuously decreased with time. The higher upwards momentum due to entrained droplets in the upflow prevented a water penetration at lower steam flow rates than in the steam only upflow case. This can be seen by comparing results of tests 13 and 12. The measured steam flow rate for onset of penetration in test 13 is in good agreement with that calculated including the entrained liquid from the core region in the upflow momentum term, and where K is set to 0. The onset of penetration is calculated by the new correlation including the entrainment upward momentum and measured for a steam mass flow rate of 116 kg/s or a steam mass flow rate per fuel assembly of 0.6 kg/s at 440 kPa pressure.

Test 15 A was performed with the same water to steam ratio, however, the core simulator injection rates were decreased and subsequently increased continuously with time in order to study the hysteresis effect. The upward steam (and water) flow rate to prevent an already established water downflow has to be higher than the onset of penetration value during decreasing steam/water upflow rate. This results from the steam condensation on the downflowing subcooled water below the tie plate which shows Fig. 12, in principle. The reduced upwards liquid momentum due to ECC water downflow contributes also to the hysteresis effect. The water injected via the core simulator nozzles (to simulate the core entrainment) within the ECC water downflow regions are flowing downwards with the ECC water and cannot contribute to the upward momentum. A redistribution of the upflowing water is much lower than for the steam upflow due to the higher inertia of the water droplets. These two effects are resulting in the difference of steam mass flow rate for onset of water downward penetration between decreased and increased upflow in tests 13 and 15 A. The hysteresis is important for the effectiveness of the reactor ECC systems, since ECC water penetrates into the core region prior to an increasing steam production due to the evaporation of this water. Thus, an increasing steam/water upflow occurs in the case of reactor ECC.

In test 15 A, in which the hysteresis effect was investigated, the established ECC water downflow could not be prevented completely by increasing the steam flow up to 200 kg/s and water flow up to 830 kg/s. In order to prevent water downflow the steam mass flow has to be increased to compensate for the condensed steam flow rate at the downflowing subcooled ECC water.

Fig. 16 shows the average reactor steam upflow during core reflood to compare with the lowest steam upflow necessary to prevent ECC water penetration. It is obvious that ECC water downflow cannot be prevented on the UPTF scale, at least not in the increasing steam/water upflow case, even if the entrainment is relatively high (test 15 A). The average steam upflow during core reflood is much lower than the steam only upflow rate necessary to prevent ECC water penetration from the upper plenum into the core region on UPTF.
scale (tests 10 A and 12). On the other hand, for small scale facilities lower than one-half linear scale (lower than one-fourth flow cross section scale), water penetration will be prevented at typical reflood steam flow rates. This is the case for most of the experimental facilities which served to study ECC system effectiveness in the past. The UPTF experiments have shown that small scale facilities cannot simulate the representative reactor scale hot leg ECC penetration into the core region.

The observed scaling effects for vertical countercurrent flow through the downcomer and the upper tie plate were the most significant ones. Three different scaling regions have been identified with increasing scale of the test facility. However, only one scaling region was observed for the following steam-water distributions and geometries.

4.2 Entrainment and Deentrainment in the Fuel Assembly Region

UPTF separate effects tests were performed to study the entrainment and deentrainment in the fuel assembly region. These tests simulate core reflood without hot leg ECC injection, i.e. with cold leg or downcomer ECC injection. Water in the core evaporates, the produced steam flows upward to the upper plenum and entrains liquid droplets. The UPTF tests were performed injecting steam and water via core simulator nozzles which distributed the steam and the water droplets uniformly over the flow cross section. The water mass flow rate falling into the lower plenum was determined dependent on core simulator steam and water injection flow rates.

The difference to the previously described tests is the homogeneous distribution of water droplets within the steam upflow compared with heterogeneous steam-water distribution. That difference causes a different flooding dependence to describe the large scale water downflow rate in the case of deentrainment in the region of the fuel assembly.

In the case of homogeneous vertical steam-water-countercurrent flow, the Kutateladze-type equation (K-correlation) can be applied to correlate all data from small scale up to large UPTF scale as described in /8/ and /18/:

\[ K_{w}^{1/2} + mK_{l}^{1/2} = C \]

UPTF data can be described using \( C = 1.64 \) and \( m = 0.77 \). The superficial velocities in the flooding correlation are calculated for the UPTF dummy fuel assembly flow cross section.

Computer programs use an average droplet diameter to calculate the interfacial area, interfacial shear force and heat transfer. If the Kutateladze-type correlation describes droplet flooding behaviour, the stable droplet diameter in the flooding location can be derived under the assumption that the droplets are spherical:

\[ d_{\text{ave}} = \frac{8}{C_{w}^{*}} \left[ \frac{\sigma}{g \left( \rho_{1} - \rho_{2} \right)} \right]^{1/2} \]
The droplet diameter can be determined by the above equations to be about the size of the Laplace length of 2.5 mm at 0.4 MPa pressure.

4.3 Countercurrent Flow In The Hot Leg During Reflux-Condenser Conditions

Reflux condensation is a cooling mode which may occur during a small break LOCA. Steam is generated in the core and flows to the steam generator U-tubes where it is condensed. The secondary side of the steam generator is colder than the primary side during this phase. The condensate flows countercurrent to the steam through the hot leg back into the reactor vessel.

Countercurrent flow of steam and water during reflux condenser conditions was investigated in UPTF for pressures of 0.3 MPa and 1.5 MPa. The condensate flow was simulated by injection of saturated water into the steam generator simulator. This water is flowing back to the upper plenum countercurrent to steam injected via the core simulator flowing from the upper plenum through the hot leg to the steam generator simulator as shown in Fig. 17. The flooding curve was measured under these conditions in the UPTF hot leg. The flooding location is obviously in the inclined part of the hot leg to the steam generator inlet (angle to the vertical axis = 40°).

As described in /8/, the flooding data can be described by a Wallis-type equation for different sizes of test facilities

\[ \frac{j_s}{j_i} + m \cdot \frac{j_s}{j_i} = C_1 \cdot \left( \sin 40° \right)^{u4} \]

with \( m = 0.7 \cdot 1.0 \) and \( C_1 = 0.61 - 0.75 \).

The dimensionless phase velocity \( j^* \) is equivalent to a Froude number modified by the density ratio of the steam and water phase. Consequently, the countercurrent flow in horizontal or inclined pipes is scaling dependent, and the scaling dependence is represented by a Froude-number. The pipe dimensions of some small scale integral test facilities are already designed according to a Froude scaling.

The Kutateladze-type equation considering instabilities of the gas-liquid interface in horizontal countercurrent flow conditions can be transferred into the Wallis-type equation which is described in /16/. This is possible for horizontal or inclined flow since the gravity force of the waves is acting perpendicular to the main flow directions of steam and water and counter the pressure difference between the bottom of the wave and the crest (Fig. 9). This means the Wallis correlation can also be applied for countercurrent flow characterized by unstable waves in horizontal oriented or inclined large pipes. Hence, there is the Wallis correlation applicable to horizontal countercurrent flow over the whole scaling region, and there is no change to the Kutateladze number criterion or to the new correlation for heterogeneous flow in contrast to vertical countercurrent flow as described above. This was confirmed by these UPTF countercurrent flow experiments investigating reactor scale hot leg reflux condenser conditions.
Figure 18 shows the maximum steam mass flux for complete turnaround of water, i.e. change from countercurrent to cocurrent flow dependent on the pipe diameter scaling. Included are some small scale test results /17, 18/. If the dimensions of the horizontal pipes of small scale test facilities are designed according to flow cross section scaling instead of Froude scaling, lower water backflow superficial velocities may result compared with UPTF full scale data. This shows the comparison with the steam mass flow of 2% core decay power scaled from 8 MPa to 0.3 MPa pressure, and assuming that only one steam generator is active. Small scale facilities below 1/3 diameter scale or 1/8 flow cross section scale would show that condensate cannot flow back to the reactor vessel, however, water delivery occurs in large scale.

4.4 Summary of Important UPTF Data

Test data of the full size UPTF are compared with small scale test data. The comparison shows scale dependent and independent phenomena.

The most significant scaling dependence is observed for vertical asymmetrical heterogeneous countercurrent flow in the downcomer and upper tie plate. The flow is asymmetrical if the Emergency Core Cooling (ECC) water is injected via the cold or hot legs and the steam-water mixture is flowing out of one broken cold or hot leg. The ECC water downflow to the core region may be limited due to high steam upflow mainly in the case of large break loss of coolant accidents. Considerable differences have been observed if the UPTF data are compared to the countercurrent flow behaviour extrapolated from small scale data to reactor scale under these conditions. The UPTF experiments have shown that small scale facilities cannot simulate the representative reactor scale ECC penetration into the core region. An increased ECC water downflow on reactor scale has been observed versus small scale behaviour.

UPTF countercurrent flow data in the case of ECC water injection via hot and cold legs cannot be described by applying the well known Wallis-type or Kutateladze-type flooding correlations. A new correlation is proposed for asymmetric heterogeneous countercurrent flow in both the downcomer and the tie plate region in the case of ECC water injection. The new correlation describes the dependence on geometrical scale and the dependence on asymmetry of the up- and possible downflow by considering the distance between the individual legs where ECC water is injected and the broken leg.

The new correlation can describe the scaling dependent heterogeneous asymmetric countercurrent flow in good agreement with UPTF data including:

- saturated water injection (basis for flooding correlations to investigate the geometrical scale effect)

- subcooled water injection (relevant for nuclear reactor ECC performance)
- upward entrainment from below the flooding flow cross section (relevant for nuclear reactor ECC performance; higher influence on ECC water downflow in large scale facilities than in small scale).

In contrast to the significant scaling dependence in vertical asymmetrical heterogeneous flow no scale dependence was observed for homogeneous steam-water flow distribution. This is the case for entrainment and deentrainment in the core region and upper plenum if the water is distributed approximately uniform over the flow cross section in the steam flow from the core. The deentrainment or water downflow can be described by the classic Kutateladze-type (K-) correlation. The Kutateladze number does not contain a geometrical parameter of the flow channel, thus the correlation is independent of different geometrical sizes. This is valid up to UPTF scale.

A different scaling dependence was observed for countercurrent flow in the hot leg under reflux-condenser conditions. The flow distribution of steam and water is heterogeneous. The steam-water countercurrent flow in horizontal and inclined pipes (inclined at the steam generator inlet) in UPTF compared with small scale results is dependent on the Froude number modified by density ratio of the steam and water phase. Such a dependence can be described by the classic Wallis-type (W-) flooding correlation over the total scaling region up to UPTF scale. This means the countercurrent flow behaviour or the backflow superficial velocity of water from the steam generator to the reactor vessel at different steam superficial velocities is dependent on the pipe diameter.

If the phenomena are scale dependent small scale test facility simulation of refill and reflood processes may result in conservative data compared to the UPTF countercurrent flow data presented here. However, in order to reach the goal of "best estimate" predictions for full size nuclear reactors and in order to reduce scale-up uncertainties these UPTF results are important for the validation of computer codes. On the other hand "distortion effects", for example, appear in experiments of small scale horizontal perforated plates or upper tie plates and upper end box configurations of a fuel assembly. These may have an important influence on countercurrent flow data caused by slots close the flow channel wall (and number of holes or hole cross sections close to the flow channel walls). Some information on this effect can be found in /19/. These effects, however, are no more important in large scale facilities like UPTF.

5 CONCLUSIONS

Countercurrent flow at different locations of the reactor primary coolant system has been investigated in the present paper. Scale dependences are shown which are different for

- vertical flow with homogeneous or heterogeneous steam-water distribution, and

- horizontal or inclined flow in pipes.
The most significant scaling dependence is observed for vertical asymmetrical heterogeneous countercurrent flow in the downcomer and upper tie plate. This scaling effect is mainly due to a pronounced multidimensional steam-water flow in a large flow passage combined with nonuniform geometric distribution of water inlet and flow outlet. The local relationship between steam upflow and water downflow will not be different between small and large scale (if no wall effects are significant), however, the pressure and velocity field in the total passage is dependent on scale. A new correlation is proposed to describe the asymmetric heterogeneous gas-liquid countercurrent flow in the total downcomer and tie plate regions. The correlation reveals and quantifies the main scaling dependent parameters determining the countercurrent flow phenomenon.

In most cases data from small scale facilities cannot directly be transferred to full scale (exception: deentrainment in vertical homogeneous flow, for example). The scaling law must be known and experimentally proven to draw conclusions from small scale test results. The scaling laws can be derived from separate effects tests. However, many phenomena interact in reactor transients, and one specific phenomenon may dominate the complete transient behaviour in integral test facilities. We have seen that ECC injection performance cannot be simulated in a "best estimate" way in small scale facilities, and therefore counterpart tests restricted to only small scale integral tests cannot reveal the "best estimate" behaviour in full size facilities. Finally some full scale data as obtained from UPTF are needed.

The most promising way of scale-up is to calculate by computer codes the integral facility behaviour covering the interactions of a variety of dominating phenomena with the response of important components. This requires adequate constitutive equations proven for the entire scale of code application. Some computer codes are using flow maps not explicitly but implicitly in their constitutive equations /20/. They are sometimes interrelated with the nodalisation chosen to describe facility behaviour.

A complete code validation processing scale dependent phenomena results up to the full plant size is still pending. The first calculations of UPTF tests have shown that in most computer codes constitutive equations hitherto have been based on informations from small scale test facilities.

The codes were not able to predict some significant phenomena of full scale experiments, e.g. vertical flooding in the downcomer and upper tie plate with heterogeneous steam-water distribution. This was also observed for codes which feature a three-dimensional representation of the reactor vessel like TRAC-PF1. The TRAC code, for example, has difficulties to identify the compact ECC water flow out of the cold legs in the downcomer or out of the hot legs in the upper plenum. The higher interfacial area of calculated dispersed flow results in higher calculated interfacial shear force. Consequently, the calculated water downflow is lower than measured. Some work has already been done to improve the codes in this respect and some work will still go on to achieve better "best estimate" calculation results.
Nomenclature

A  flow cross section (m²)
Bo  Bond number
\( c_p \)  specific heat at constant pressure (kJ/kgK)
\( d_w \)  downcomer average diameter (m)
\( d_e = \left( \frac{4A_m}{\pi} \right)^{1/2} \)  equivalent diameter of the tie plate flow cross section (m)
f  condensation efficiency (-)
Fr  Froude number
g  gravitational acceleration (m/s²)
\( h_g \)  enthalpy change of vaporization (kJ/kg)
j  superficial velocity (volume flow rate/total flow cross section) (m/s)
L  characteristic geometrical length (m)
M  mass flow rate (kg/s)
p  pressure (kPa)
Re  Reynolds number
T  temperature (K)
\( \alpha \)  volumetric vapour fraction (-)
\( \theta \)  angle between ECC injection leg and broken leg (*) or angle between vertical direction and flow channel inclination
v  kinematic viscosity (m²/s)
\( \rho \)  density (kg/m³)
\( \sigma \)  surface tension (N/m)
Dimensionless parameters

\[ k^* = \frac{h \rho_{L2}^2}{\left[ g \mu_2 (\rho - \rho_v) \right]^{\frac{1}{2}}} \]

- dimensionless superficial velocity based on equivalent diameter
- \( x = g \) or \( l \)

\[ J_x^* = \frac{h \rho_{L2}^2}{[8 \rho_2 (\rho - \rho_v)]^{\frac{1}{2}}} \]

- dimensionless superficial velocity based on average downcomer annulus circumference
- \( x = g \) or \( l \)

\[ K_x^* = \frac{h \rho_{L2}^2}{[8 \rho (\rho - \rho_v)]^{\frac{1}{2}}} \]

- dimensionless superficial velocity based on surface tension (Kutateladze number)
- \( x = g \) or \( l \)

Subscripts

- cond: condensation
- cs: core simulator
- e: equivalent
- ECC: emergency core cooling water
- g: gas
- l: liquid
- m: mixture (gas/liquid)
- sat: saturation
- TP: tie plate
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<table>
<thead>
<tr>
<th>Facility</th>
<th>Country</th>
<th>Owners</th>
<th>Max. Power (MWth)</th>
<th>Volume Scaling Ratio</th>
<th>Primary Volume (m³)</th>
<th>Pressure (MPa)</th>
<th>Number of Loops</th>
</tr>
</thead>
<tbody>
<tr>
<td>PMK-NVH</td>
<td>Hungary</td>
<td>KFKI-Budapest</td>
<td>2.0</td>
<td>2070</td>
<td>0.13</td>
<td>16</td>
<td>(6)</td>
</tr>
<tr>
<td>SEMISCALE</td>
<td>USA</td>
<td>INEL-Idaho</td>
<td>2.0</td>
<td>1600</td>
<td>0.2</td>
<td>15</td>
<td>1+(3)</td>
</tr>
<tr>
<td>MIST (2x4 Loop)</td>
<td>USA</td>
<td>B &amp; W</td>
<td>0.34 *)</td>
<td>840</td>
<td>0.56</td>
<td>15.5</td>
<td>2 hot, 4 cold</td>
</tr>
<tr>
<td>LOBI-MOD2</td>
<td>CEC</td>
<td>CEC-Ispra</td>
<td>5.4</td>
<td>700</td>
<td>0.6</td>
<td>15.5</td>
<td>1+(3)</td>
</tr>
<tr>
<td>UMCP (2x4 Loop)</td>
<td>USA</td>
<td>Univ. of Maryland</td>
<td>0.2 *)</td>
<td>500</td>
<td>0.6</td>
<td>2.1</td>
<td>2 hot, 4 cold</td>
</tr>
<tr>
<td>SPES</td>
<td>Italy</td>
<td>SIET-Placentia</td>
<td>9.0</td>
<td>420</td>
<td>0.63</td>
<td>20</td>
<td>1+1+1</td>
</tr>
<tr>
<td>PKL III</td>
<td>Germany</td>
<td>KWU</td>
<td>2.5 *)</td>
<td>145</td>
<td>2.9</td>
<td>4.0</td>
<td>1+1+1+1</td>
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<tr>
<td>BETHSY</td>
<td>France</td>
<td>CEN-Grenoble</td>
<td>3.0 *)</td>
<td>100</td>
<td>2.88</td>
<td>17.2</td>
<td>1+1+1</td>
</tr>
<tr>
<td>LOFT</td>
<td>USA</td>
<td>INEL-Idaho</td>
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<td>50</td>
<td>7.9</td>
<td>15.5</td>
<td>(2)+1</td>
</tr>
<tr>
<td>ROSA-IV</td>
<td>Japan</td>
<td>JAERI</td>
<td>10 *)</td>
<td>48</td>
<td>7.2</td>
<td>16</td>
<td>(2)+(2)</td>
</tr>
<tr>
<td>PWR (typical)</td>
<td>--</td>
<td>--</td>
<td>3800</td>
<td>1</td>
<td>350</td>
<td>16</td>
<td>1+1+1+1</td>
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</table>

Table 1: PWR Integral Loop Test Facilities and Main Characteristics

*) Scaled full power not possible
(number) = Lumped loops with corresponding flow capacity
<table>
<thead>
<tr>
<th>Facility</th>
<th>Volume</th>
<th>Elevations</th>
<th>Flow Cross Sections</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Core</td>
<td>SSG</td>
</tr>
<tr>
<td>PMK-NVH o)</td>
<td>2070</td>
<td>1</td>
<td>1 (horiz.)</td>
</tr>
<tr>
<td>SEMISCALE</td>
<td>1600</td>
<td>1</td>
<td>1-1.07</td>
</tr>
<tr>
<td>MIST (2x4 Loop)</td>
<td>620</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>LOBI-MOD2</td>
<td>700</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>UMCP (2x4 Loop)</td>
<td>500</td>
<td>6.6</td>
<td>4</td>
</tr>
<tr>
<td>SPES</td>
<td>420</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>PKL III</td>
<td>145</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>BETHorny</td>
<td>100</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>LOFT</td>
<td>50</td>
<td>2</td>
<td>n.a.</td>
</tr>
<tr>
<td>ROSA-IV (LSTF)</td>
<td>48</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>PWR (typical)</td>
<td>1</td>
<td>1</td>
<td>1</td>
</tr>
</tbody>
</table>

Table 2: Scaling Ratios "Prototype/Model" for PWR Integral Loop Test Facilities

o) related to WWER-440/230

() lumped loop components
Table 3: Classic Flooding Correlations

PREVIOUS SMALL SCALE EXPERIMENTS HAVE PROVIDED FLOODING CORRELATIONS:

For Downcomers:

\[ J^* \sim 1/2 \times 0.8 \frac{J^*}{\overline{t}} = 0.4 \]

[\text{P. H. Rothe, 1979}]

\[ 0 \leq \frac{d_{av}}{\overline{t}} \left[ g \left( \rho_1 - \rho_g \right) / \sigma \right]^{1/2} \leq 400 \]

K-Correlation

\[ K_g^{1/2} = 0.82 K_1^{1/2} = 1.80 \]

[C. J. Crowley, P. H. Rothe, R. G. Sam, 1981]

\[ 400 \leq \frac{d_{av}}{\overline{t}} \left[ g \left( \rho_1 - \rho_g \right) / \sigma \right]^{1/2} \text{ and } g^{1/2} L / V_g^{2/3} \leq 5400 \]

For Upper Tie Plates:

\[ J^* \sim 1/2 \times 1/2 = 1.0 \]

\[ 0 \leq \frac{d_s}{\overline{t}} \left[ g \left( \rho_1 - \rho_g \right) / \sigma \right]^{1/2} \leq 10 \]

K-Correlation

\[ K_g^{1/2} = 0.77 K_1^{1/2} = 1.80 \]

(Karlestein-Data)

\[ 10 \leq \frac{d_{av}}{\overline{t}} \left[ g \left( \rho_1 - \rho_g \right) / \sigma \right]^{1/2} \text{ and } g^{1/2} L / V_g^{2/3} \leq 4400 \]

However, flooding behavior is scaling dependent and above correlations cannot be extrapolated to full-size reactor conditions.
Table 4: New Flooding Correlations for Downcomer and Upper Tie Plate

NEW FLOODING CORRELATIONS FOR FULL-SCALE CONDITIONS WERE DEVELOPED FROM UPTF DATA

- **Downcomer Correlation**

  \[
  K_g^{1/2} \left( \frac{\nu_4^{2/3}}{g^{1/3} L} \right)^{1/2} + 0.011 K_1^{1/2} = 0.0245
  \]

  \[
  5 \leq \frac{g^{1/3} L}{\nu_4^{2/3}}
  \]

- **Upper Tie Plate Correlation**

  \[
  K_g^{1/2} \left( \frac{\nu_4^{2/3}}{g^{1/3} L} \right)^{1/2} + 0.014 K_1^{1/2} = 0.027
  \]

  \[
  L \leq \frac{g^{1/3} L}{\nu_4^{2/3}}
  \]

New correlations based on classic Kutateladze-Type correlations
Table 5: Consideration of Multiple ECC Water Injection Locations

\[
\frac{K_i \cdot \bar{P}}{\gamma_{w,ih}} = \frac{K_i}{L_i} = \frac{L_1 \cdot K_1 / L_1 + L_2 K_2 / L_2 + L_3 K_3 / L_3}{L_1 + L_2 + L_3}
\]

- \( L_i = 0 \) if no ECC water is injected into CL or HL, or
- if \( K_i / L_i \geq 0.0245 \) for downcomer or
- if \( K_i / L_i \geq 0.027 \) for tie plate

- \( i = 1, 2, 3 \)

- \( L_i \) distance between the respective cold or hot leg with ECC water injection and the respective broken cold or hot leg
Table 6: Consideration of Condensation

$j^*$, $J^*$ and $K$ correlation:

\[
J_g = J_g,\text{core} - J_g,\text{cond}
\]

\[
J_g,\text{cond} = f \left( T_{\text{sat}} - T_{\text{ECC}} \right) \frac{c_p M_{\text{ECC}}}{\rho_0 \rho_s A}
\]

Consideration of Entrainment

\[
J_g \rho_g^{1/2} = \frac{\dot{m}_g n_0 T}{\rho_g^{1/2} A}
\]

\[
\rho_m = \alpha \rho_g + (1 - \alpha) \rho_l
\]

\[
\alpha = \frac{\dot{m}_g/\rho_g}{\dot{m}_g/\rho_g + \dot{m}_l/\rho_l}
\]
Fig. 1: Selected Ratios of Hardware Dimensions (Prototype/Test Rig) of Various Integral Loop Test Facilities

- Geodetic Elevations
- Hot Leg Flow Cross Sections
- Core Flow Cross Sections
- Hot Leg Diameters

Multicore Loops indicated by C)
TWO METHODS USED FOR ECC IN PWR

Cold leg injection into downcomer

Hot leg injection above upper tie plate

Cooling water must flow countercurrently to steam generated within core

Fig. 4: Downcomer and Upper Tie Plate Countercurrent Flow
Fig. 5: ECC Water Cold Leg Injections into the Downcomer
Fig. 6: Downcomer Flow Distribution for ECC Water Injection into Cold Legs 1, 2 and 3 (Test 6)
Fig. 7: ECC Water Hot Leg Injections into the Upper Plenum above the Upper Tie Plate
Time (s) = 220.00

UPTF Test 12
(Run 014)

Fluid temperatures
10 mm below tie plate
DT = Tsat - Tfluid (K)

Fig. 8: Upper Tie Plate Flow Distribution for ECC Water Injection Into Hot Legs 1, 2 and 3 (test 12)
Fig. 9: Waviness of the gas/liquid interface in countercurrent flow
Fig. 10: Downcomer Flooding Correlations for Zero Penetration of Liquid (Total Bypass) for all Scale Facilities
Fig. 11: Upper Tie Plate Flooding Correlations for Zero Penetration of Liquid for all Scale Facilities
CONDENSATION

Fig. 12: Condensation Effect on Countercurrent Flow
DOWNCOMER (UPTF: $A_{DC} = 3.628m^2$)

- ECC: CL 1, 2, 3 $p = 360-1120$ kPa
- ECC: CL 1 $p = 286-498$ kPa
- ECC: CL 2, 3 $p = 330-416$ kPa
- ECC: CL 1, 3 $p = 398$ kPa
- ECC: CL 1, 2, 3 $p = 337-414$ kPa

UPTF Test 6

UPTF Test 7

Onset of Penetration

Own Correlation

Test 5B

Fig. 13: Own Flooding Correlation Derived from Saturated Water Downcomer Data and Subcooled ECC Water Test 5B
Fig. 14: Own Flooding Correlation Derived from Saturated Water Upper Tie Plate Data and Subcooled ECC Water Test 12
ENTRAINMENT

\[ \dot{M}_l + \dot{M}_g + \dot{M}_l = j_m \rho_m^{1/2} \frac{\dot{M}_g + \dot{M}_l}{\rho_m^{1/2} A} \]

Fig. 15: Effect of Entrainment from below the Flooding Location
UPPER TIE PLATE (ONSET OF PENETRATION)
p = 400 kPa

![Graph showing core simulator steam mass flow rate per fuel assembly.]

- Test 10A, 12 calculated
- Test 15, \( \dot{M}_{f} / \dot{M}_{g} = 4 \), measured increased upflow
- Average reactor steam upflow during core reflood
- Saturated ECGW
- Test 13, \( \dot{M}_{f} / \dot{M}_{g} = 4 \), decreased upflow, measured and calculated

Tie plate equivalent diameter scale

Fig. 16: UPTF Tie Plate Data and All Scale Correlations for Zero Liquid Penetration Compared with Reactor Steam Uplow
Fig. 17: Countercurrent Flow in Hot Leg during Reflux Condenser Conditions
Water Turnaround Limit in Hot Leg ($p = 0.3$ MPa)

→ dependent on Froude scaling ($f^*$ correlation)

Steam Mass Flow Rate for $D = 0.76$ m

kg/s

36.2

27.0

20.4

16.5

kg/s

1:10

1:3

1:1

Chmut

Richter

Condenser Mass Flux (1 active SG)

Complete Turnaround

Water Delivery

UPTF Test 11

Scaling

$D_{facility}/D_{reactor}$

Fig. 18: Countercurrent Flow Limitation Correlation compared with Reactor Steam Mass Flow in the Hot Leg
Large-scale multi-dimensional phenomena found in CCTF and SCTF experiment

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Tokai-mura, Naka-gun, Ibaraki-ken, 319-11, Japan

Abstract

Thermal–hydraulic behaviors in pressure vessel during reflood phase of a PWR–LOCA are discussed based on the results from tests with Cylindrical Core and Slab Core Test Facilities, which model a 1100–MWe-class PWR with scaling ratio of about 1/20.

Major findings on core thermal–hydraulics are, i) much water accumulation in the upper part of the core, ii) radially uniform water accumulation, and iii) fluid circulation and/or concentration to high power bundles. These multi-dimensional phenomena cause better core cooling in high power bundle than expected from an evaluation model for licensing based on one-dimensional reflood experiments. The much water accumulation is considered to be caused by recirculation flow by comparing with data of the FLECHT low-flooding tests.

Based on small scale reflood experiment, effect of local mass flow on heat transfer enhancement in a film boiling was correlated and all phenomena except the flow concentration effect were numerically well-simulated by using an analytical model with this correlation. The flow concentration effect on core heat transfer was empirically correlated with local power ratio.

It was observed that non-uniform fluid mixing between core flow and subcooled water from hot legs for PWRs with combined-injection ECCS, which enhances the core coolability. This is multi-dimensional phenomena. The heat transfer enhancement was also well-simulated with the above-mentioned model.

1. Introduction

It was expected that during a reflood phase of a PWR–LOCA more excellent core cooling would be realized in reactor cores than in one-dimensionally simulated test sections due to mixing or circulation of coolant in the core and reduction of steam binding due to deentrainment of carried water from the core. These effects are generally termed as a multi-dimensional effect. To investigate the effect some reflood tests have been performed with relatively large test sections. One of famous tests is the FLECHT tests [1], [2] with simulated cores with 100 simulated fuel rods (volumetric scaling ratio of about 1/430) and by using the results of the tests present licensing models for reflood analysis were developed and the model is used in WREM code [3]. However it was still wondered whether the observed phenomena were enough representing phenomena in reactors.

In the present paper, the multi-dimensional thermal–hydraulic behaviors will be discussed based on the results of large scale reflood tests for PWRs with both the cold leg and combined injection types of ECCS by using the Cylindrical Core Test Facility (CCTF) and the Slab Core Test Facility (SCTF) with scaling ratio of about 1/20 in volumetric scaling. The core thermal–hydraulics coupled with system behavior in the primary loops has been investigated by using the CCTF. The detailed multi-dimensional thermal–hydraulic behavior with emphasis on flow in radial direction in the reactor core is investigated using the SCTF with a full height, full radius and one-bundle–width PWR–simulated core. The core
thermal–hydraulics will be investigated based on detailed information from more than 1600
detectors and analysis with existing one–dimensional correlations and data. To confirm the
reasonableness of our physical understanding, the experimental results will be quantitatively
compared with the results of numerical simulation by computer codes with modified models
included the multi–dimensional effects. Finally the safety margin in the licensing analysis
code like the WREM code will be discussed.

2. Experimental

2.1 Test facility

The CCTF and SCTF were utilized for investigating the multi–dimensional reflooding
phenomena. They are main facilities of the Large Scale Reflood Test Program which was
initiated in April, 1976 [4].

1) Cylindrical core test facility (CCTF)

The CCTF [5] was designed to provide the capability to reasonably simulate the flow
conditions in the primary system of a PWR during the refill and reflood phases of a LOCA,
and models a four–loop 1100 MWe PWR with the flow area scaling ratio of 1/21.4. It has
a scaled pressure vessel with a full height core and four loops with passive and active
component simulators and containment tank. The core has about 2000 electrically heated rods
arranged in cylindrical configuration. Each heated rod is full–size 15 x 15–array fuel rod
simulator and has an axial power distribution with a peaking factor of 1.4. The core can be
electrically subdivided into three regions to achieve a desired radial power profile.

2) Slab core test facility (SCTF)

The SCTF [6] was operated to complement the results from CCTF specially in the field
of the multi–dimensional thermal–hydraulic behavior in a radially wide core and upper
plenum. SCTF has a full height, full radius and one–bundle width of a PWR–simulated core
which is composed of in–line eight bundles and has downcomer, upper and lower plena and
simplified primary loop components. Only one full–height and scaled–area hot leg piping,
which connects the upper plenum and a steam water separator, is provided to simulate four
hot legs of a PWR. The separator is provided instead of active steam generators. One intact
loop with a pump simulator connects the separator and represents three intact loops. The
vessel side and the separator side broken cold legs are connected to respective containment
tanks. A core bundle has 16 x 16 heated rods including 22 non–heated rods. Each heated
rod is a full–size 15 x 15 – array fuel rod simulator. The power supplied to each bundle can
be independently controlled to achieve a desired radial power profile.

2.2 Referred CCTF and SCTF tests

The following CCTF and SCTF tests are referred in the present study: Concerning the
simulation of PWRs with cold leg injection type ECCS, CCTF EM (Evaluation Model) test,
CCTF base test, SCTF steep power test, SCTF flat power test and SCTF inclined power test
are referred. Concerning the simulation of PWRs with combined injection type ECCS, CCTF
combined injection test and SCTF combined injection test are referred.
The CCTF EM test was performed under test conditions which were equivalent to the initial and the boundary conditions predicted in the safety evaluation analysis. In this test the core radial power distribution was set steep (Maximum bundle power ratio = 1.29). The CCTF base test was performed with flatter core power distribution (Maximum bundle power ratio = 1.15) than CCTF EM test. The SCTF steep power test, flat power test and inclined power test were performed under similar core inlet boundary conditions with each other. The major difference is in the core power distribution. The maximum bundle power ratio is 1.2, 1.0, 1.36, respectively. The CCTF and SCTF combined injection tests are performed to study the system behavior and the detailed thermal-hydraulics in the pressure vessel, respectively.

2.3 WREM code

WREM code [3] is widely used for the reactor safety analysis. The calculation with WREM code is performed with two steps. In the first step, the system calculation is made and the boundary conditions at the core inlet (core bottom) are obtained. In the second step, the calculation for the "hot rod" (highest power fuel rod) in the core is made by using the core inlet boundary conditions (core flooding rate, core inlet fluid temperature and core inlet pressure) obtained in the system calculation. The peak clad temperature is calculated in this step.

3. Results and discussion

3.1 Multi-dimensional effect for PWRs with cold leg injection type ECCS

1) Core cooling behavior found in the CCTF tests [17]

Figure 1 shows the comparison of the clad temperature transient between the measurement and the prediction with the WREM code for CCTF EM test. The comparison is made at the location where the linear power density is the highest in the hot rod. The pretest calculation gives much higher clad temperature than the experiment. It is therefore confirmed that the WREM code conservatively predicts the core cooling observed in the CCTF.

In the post-test calculation, the boundary conditions at the core inlet were given based on the measured values of CCTF test. It is found that no significant discrepancy can arise from the system calculation of the WREM code. The difference in clad temperatures between the post-test calculation and the experiment is caused only by the core thermal-hydraulic model in the WREM code. This indicates that the conservatism of WREM code originates mainly in the core calculation and that the system calculation of the WREM code itself has only a small conservatism.

2) Comparison of hydraulic behavior in the CCTF and the FLECHT tests

Figure 2 shows the measured and calculated total differential pressures in the core. At the beginning of the transient, the calculated is about half of the measured and the difference becomes smaller and in later period the calculated becomes larger than the measured. Therefore it was considered that the difference of hydraulic behavior in the core is one of reason of the better core cooling, that is, the multi-dimensional effects. In order to identify the reason of the better core cooling in CCTF, the FLECHT test data [2], which were
experimental data base for modeling of the WREM code, and calculational scheme of the WREM code were reviewed. From this effort, the following three items were identified as the main reasons:

i) conservatism of the FLECHT heat transfer coefficient correlation in comparison with CCTF data,

ii) radially uniform water accumulation due to good horizontal water mixing among subchannels, and

iii) heat transfer enhancement due to radial core power distribution.

In the WREM code, the FLECHT heat transfer coefficient correlation is used and the influences of both radially uniform water accumulation and heat transfer enhancement due to radial core power distribution are neglected. This results in much conservatism in the WREM calculation.

Conservatism of the FLECHT heat transfer coefficient correlation can be attributed to more water accumulation in the upper part of the CCTF core than expected from the FLECHT data, as shown in Fig.3. In the FLECHT test the averaged void fractions evaluated from the differential pressure of measuring sections of 0 to 0.61 meter, 0.61 to 1.22 meters and 1.22 to 1.83 meters start to decrease when the averaged void fractions of the lower section reduce the decreasing rates and after the flow housing is totally quenched the void fractions of all upper sections start to decrease almost simultaneously, while in the CCTF void fractions of all sections start to decrease simultaneously. This indicates significant density difference exists below and above the quench front in the FLECHT tests before the quench of the flow housing and density difference is relatively small in the CCTF. It is considered that more water accumulation above the quench front can be realized in the CCTF than in the forced-feed FLECHT reflood tests with a hot flow housing. This then leads to better core cooling in the CCTF. The reason of more water accumulation in the upper part of the CCTF core will be discussed later.

In the later period, the total core differential pressure predicted with WREM code became higher than the measured in the CCTF. This is explained as follows: In the prediction the core inlet subcooling was input and set 64K, while in the CCTF less subcooled water entered the core due to incomplete mixing of water in the lower plenum. Therefore, it is considered that the predicted higher differential pressure in the core does not indicate the higher water accumulation above the quench front but the higher water accumulation below the quench front introduced by a longer subcooled region.

3) Characteristics of water accumulation in the upper part of the core

The void fraction transients of FLECHT lower flooding test show two types of water accumulation core, i.e. Type 1 and Type 2 as shown in Fig. 4. In the FLECHT test, as shown in Fig. 5, Type 2 appeared in the cases where the flooding rate was equal to or higher than 4 cm/s. This suggests that the steam-supplied drag force to the liquid is not enough to accelerate water to the speed of dispersed flow at high mass flow rate.

In JAERI small scale reflood test and FLECHT-SET [7] test, the flow housing was not heated and kept saturation temperature in order to avoid additional steam generation due to the heat release from the housing wall. This must be the reason why Type 2 appeared in these tests.

As illustrated in Fig. 6, a liquid film formed on the quenched wall can capture water droplets and the water can fall down to the top of froth flow or the quench front of rod bundles. This reflux water flow increase the mass flow to be accelerated at the droplet generation location, i.e. quench front or top of froth flow region. Except for the beginning
of the transient, the flooding rate was evaluated to be 2cm/s in the CCTF. Even if the flooding rate is 2 cm/s, the mass velocity become 4 cm/s in the case of 50 percent capture of water droplets and water accumulation type become Type 2. Once Type 2 is established, large water droplets or liquid slug become dominant and absorb smaller droplets in larger water droplets or liquid slug. This assumption indicates that the capture of water droplets and flow path to drain the captured water down to the droplet generation location are necessary conditions to realize Type 2.

In a realistically simulated core, there are many types of structure without heat source, such as tie plates above the core and control rod guide thimbles in the core. They are almost kept at saturation temperature or they can be easily cooled down and capture water on the surface of the structure. The captured water can fall back to the quench front of the rod bundle along the outside surface of the thimbles. CCTF satisfy this condition and Type 2 appeared in the CCTF test. Accordingly we can expect that Type 2 must appear in the reflow phase of PWRs. Therefore we can think that the appearance of liquid slug flow above the quench front, that is, in post–dryout region, is the multi–dimensional effect.

Based on the JAERI small scale reflood test, the void fraction and heat transfer correlations, that is, Murao–Iguchi and Murao–Sugimoto correlation respectively the slug flow in post–dryout region [8], [9]. The REFLA code including these correlations made a good comparison between the measured and the calculated as shown in Fig.7 for the flat radial power test in the CCTF. From the predictive capability of REFLA code, it is concluded that the validity of the models in consideration with the slug flow for one–dimensional calculation was demonstrated.

4) Radial water mixing found in the SCTF

Good radial (horizontal) water mixing among subchannels and heat transfer enhancement due to radial core power distribution are confirmed in the full radius core of the SCTF.

The behavior of the radial water mixing in the SCTF is clearly identified from Fig.8, where the isobar lines of 0.5kPa step are indicated. Since the fluid velocity is not so high during reflood phase, the pressure difference is almost due to static head of water accumulation. Therefore, the narrower gaps between two adjacent contours indicate the more water accumulation. The cross marks shown in the figure indicate the estimated isobar by using the void fraction correlation for the slug flow with assuming no radial fluid mixing. The inclination of the estimated result is much steeper than the measured isobar. This means that the radial water mixing over the entire core occurs and the radially uniform water accumulation is realized even in the full radius of PWR core.

Since the water accumulation is one of the essential factors for the heat transfer, the above mentioned water mixing results in the increase of the heat transfer in the high power bundle, and then this heat transfer becomes closer to that in a bundle of the flat power distribution under the same average power level. However, as shown in Fig. 9, the heat transfer coefficient in high power bundle is clearly higher than that in flat radial power test. This indicates that other factor to enhance the heat transfer exists in inclined radial power test. In this test, the existence of the flow concentration to high power bundle is suggested beneath the quench front from the isobar in Fig. 8. Other nonuniform power distribution tests showed the similar tendency, that is, the flow concentration to the high power bundles and the heat transfer enhancement in the high power bundles. It is considered therefore that the heat transfer enhancement in high power bundle in PWRs is caused by the flow concentration.
5) Qualitative evaluation of core cooling enhancement due to radial power distribution [15]

In order to have a quantitative understanding of the radial power distribution effect on core cooling enhancement, the differences in heat transfer coefficients between the nonuniform radial power distribution tests and the corresponding flat radial power distribution tests are plotted against the bundlewise radial power ratios in Fig. 10. In this figure, the data at elevations of 1.83m for CCTF test and 1.905m for SCTF tests were used.

It is noted from Fig. 10 that no significant difference in this relation can be recognized between tests while the shapes of radial power distribution are different. The elevation ranged from 1.905 to 2.33m was found to have also little effect on this relation. Here, the highest clad temperature was observed at either 1.905 or 2.33m. In addition, as shown in Fig. 10, the data from SCTF and CCTF tests are well agreed with each other in spite of the difference in the power distribution shape and the core radial size. That is, the heat transfer enhancement or degradation due to the radial power distribution is governed mainly by the bundlewise local radial power ratio itself and less dependent on the power distribution shape under the same peak power ratio of 1.36. Also, the scale of core radius from 1/4.6 to 1/1 has little effect on this relation.

6) Numerical simulation of radial power distribution effect code

In the above numerical simulation the heat transfer enhancement due to radial core power distribution is not apparently considered. This phenomenon is simulated by using REFLA/TRAC code (best simulation code version of J-TRAC code) [15], where the multi-dimensionality such as a flow concentration can be analyzed due to the three dimensional scheme of the code. This simulation was performed by incorporating a new heat transfer correlation into the REFLA/TRAC code. The REFLA/TRAC code was developed by using US-developed TRAC code as a frame of the code and the reflood model of the REFLA code in order to improve the predictability of the TRAC code for multi-dimensional reflood behaviors. The correlation includes the effect of fluid velocity on heat transfer coefficient.

The prediction for the SCTF steep power test was made with and without taking into account the fluid velocity effect. As shown in Fig. 11, the prediction without this effect is higher than the measured clad temperature. Since the prediction without this effect has given good agreement with the SCTF flat power test, the discrepancy A in the figure is mainly due to the neglect of the flow concentration effect. The prediction with the fluid velocity effect is lower than that without this effect but it is still higher than data. Therefore, although the model for the flow concentration effect improves the simulation accuracy slightly, more improvement is necessary.

It is considered that the discrepancy B is due to the insufficient modeling of two effects; (1) the water flows to the quench front of the hot bundle from the adjacent bundles and (2) the water upflows vertically in the elevation above the quench front of the hot bundle. If the water flowing radially just above the quench front in the REFLA/TRAC calculation flows only upward, the prediction with the velocity effect could give better prediction for the flow concentration effect.

3.2 Multi-dimensional effect for PWRs with combined injection type ECCS

1) Core cooling behavior observed in the CCTF [16]

The CCTF gives the good simulation of the entire system of PWRs. Therefore, it is
expected that the phenomena observed in CCTF can be reasonably extrapolated to PWRs. One of multi-dimensional phenomena observed in the CCTF combined injection tests is the fluid temperature distribution in the upper plenum and the core, as shown in Fig. 12. The data were obtained during the period when the water pool was considered to form in the upper plenum. This figure shows that the fluid mixing is not uniform between the subcooled ECC water from hot legs and the saturated fluid flow from the core and the subcooled and saturated regions are forming separately in the upper plenum and the core.

In the CCTF the generated steam completely condensed in upper plenum and no water was entrained from the upper plenum to the hot legs. This facilitates the water pool formation in the upper plenum. If we assume the incomplete steam condensation occur, i)the surplus steam and the entrained water enter hot legs and the steam generators, ii)the entrained water is vaporized in the steam generators and causes suppression of the core water level due to stronger steam binding, and iii)the suppression of steam generation in the core due to the suppressed core water level causes reduction of steam flow from the upper plenum to the hot legs. Thus, the steam generation rate is controlled not to entrain the water to the hot legs and the upper plenum water pool formation is facilitated. In the system which does not induce enough steam binding to suppress the core water level, this discussion is not valid. The flow circulation mentioned below also takes the important role to sustain the water pool in case of using active heated core.

2) Core cooling enhancement found in the SCTF tests [15]

The region separation to the subcooled and saturated regions were more clearly observed in the SCTF than in the CCTF. In addition, SCTF data showed the flow circulation in the pressure vessel [11]. Figure 13 shows the region separation in the core (Water downflow region and the two-phase upflow region) and the existence of the flow circulation in the pressure vessel. The numbers in the figure were obtained from various measurement, so that each value is not quantitatively consistent with each other due to measurement errors. However, the existence of a strong flow circulation is considered to be obvious.

This flow circulation is considered to have been realized as follows: Usually the water capacity in the active heated core does not increase rapidly, since the quench front does not propagate fast and the steam flow entrains the water above the quench front in the two-phase upflow region. Hence the two-phase upflow region does not quickly collapsed. And due to the small thermal and momentum diffusions in subchannels of water fall-back region, heat and momentum in the two-phase upflow region are not significantly transferred to the water fall-back region, that is, only surface of boundary can be heated and deaccelerated but not inside of the water fall-back region and the water fall-back region does not quickly disappear. Thus two regions were separately established. The difference of densities in the two regions induced flow circulation in the core and the lower and the upper plena. The circulation flow rate is controled by the balance of differential pressure in both region. In addition, the drained water flow rate from the core bottom to the broken cold leg nozzle via downcomer is controled by the pressure in the pressure vessel, which is governed by condensation in the upper plenum, should be balanced with water fall-back and up-flow rate in the core.

The heat transfer coefficient in the two-phase upflow region is higher than the predicted with Murao-Sugimoto correlation which is developed for a PWR with cold leg injection type ECCS under rather low core flooding rate (a few cm/s) as shown in Fig. 14. Based on the JAERI small scale reflood tests of high flooding rate, Ohnuki extended the correlation for wide range of local liquid velocity 10.
3) Difference of core cooling behaviors found in CCTF and SCTF tests

The CCTF and SCTF tests for PWRs with combined-injection ECCS showed slightly different core cooling behavior, that is, SCTF test showed higher core cooling. By considering this fact and the flow map in the hot leg obtained in hot leg injection tests performed in CCTF, it was found that there is two possible ECC water discharge models from the hot leg to the pressure vessel, that is, stable discharge and intermittent discharge. The core coolability was demonstrated for both discharge mode with SCTF, however, the reason of the difference of core cooling behavior in CCTF and SCTF must be investigated.

4) Numerical prediction of core cooling

Important phenomena um12tm for core cooling in PWR with combined injection are (1) region separation, (2) flow circulation, and (3) heat transfer enhancement due to flow circulation. The thermal–hydraulics in the two-phase upflow region was predicted with the REFLA code [12], [13], [14], [19] by incorporating the newly developed models for the above (1) – (3) and assuming water fall-back flow area and drainage rate from the broken cold leg nozzle.

Figure 15 shows the predicted and the measured clad temperatures in two-phase upflow region. The prediction gives a good agreement with the SCTF data. The predicted core differential pressure is also shown in Fig.14. The agreement with the data is good for later period (>50s). For early period the agreement of differential pressure is not so good, and then some modification of hydraulic model is required. However the influence of the void fraction on the core heat transfer is weak but the influence of flow circulation or velocity is strong, if the void fraction is relatively small. This is the reason why the good core cooling prediction was obtained even under poor prediction of differential pressure in early period.

4. Conclusion

The thermal hydraulic behaviors in pressure vessel during reflood phase of a PWR–LOCA are discussed based on the test results with Cylindrical Core Test Facility (CCTF) and Slab Core Test Facility (SCTF) and the analytical results. Both facilities model a 1100–MWe–class PWR in the volumetric scaling ratio of about 1/20.

The major findings on core thermal hydraulics are i) more water accumulation in the upper part of the core than expected from models based on one-dimensional reflood experiments, ii) almost radially uniform water accumulation over the entire core due to good radial water communication, and iii) fluid circulation and/or fluid concentration to high power bundles. These multi-dimensional phenomena cause better core cooling in high power bundle than expected from models based on one-dimensional reflood experiments.

The core cooling enhancement due to radial power distribution, which is induced by flow concentration was quantitatively evaluated and the relation between increase of heat transfer coefficient and radial power ratio was derived.

Difference of core cooling behavior was observed in CCTF and SCTF tests but the core coolability has been demonstrated for both cases, however, more investigation of reason is necessary.

The above mentioned phenomena were attempted to simulate numerically. All phenomena except the effect of the flow concentration were well simulated. For better simulation on the above phenomenon, more precise model of two-phase flow is necessary.

Observed was non-uniform fluid mixing between core two phase up-flow region and
subcooled water fall-back region from hot legs and induced big flow circulation in the core and upper and lower plena for PWR with combined-injection ECCS, which enhances the core cooling considerably.

Acknowledgment

The author is deeply indebted to Drs. T. Hiraoka, Y. Ishiguro and Y. Abe, T. Okubo, A. Ounuki, K. Sato, M. Shiba of Japan Atomic Energy Research Institute for hearty suggestions and encouragement.

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(3) WREM/NUREG-75/056(1975)


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Tie plate + Wide core  

Cold flow housing  

Tie plate  

Droplet  

\( G'_e \)  

Falling liquid  

Liquid film  

Flow housing  

Droplet  

\( G'_e \)  

Falling film  

Quench front  

Liquid  

\( G_e \) (Liquid mass flux)  

\[ G_e + \eta G'_e = G'_e \]

\[ G'_e = \frac{G_e}{1 - \eta} \]

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Significant Large Scale Phenomena Identified in LOFT Experiments

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Abstract

For many years the LOFT facility played a principal role in reactor safety research providing data on Pressurized Water Reactors (PWRs) system behaviour during postulated accidents. As any scaled integral facility LOFT introduced some distortions in respect to phenomena and interactions expected in large commercial PWRs. Therefore the LOFT tests could not be regarded as demonstration of PWR behaviour but as source of broad range of data enhancing the understanding of system response phenomena and providing important thermal-hydraulic data for computer code and model assessment.

This paper provides an overview of the selected significant phenomena and interactions observed in LOFT experiments that affected understanding of the PWR system behaviour or provided valuable lessons for model development and code assessment.

LOFT experiments simulated large break LOCA, small break LOCA, anticipated transients with and without scram, and some core damage and fission product release accidents. These experiments showed importance of such phenomena as early blowdown quenches during large break LOCA, pump operation and break location effects on the coolant inventory for small breaks, natural circulation cooling during small break LOCA or ECC effectiveness and distribution. These experiments provided also insight into processes such as primary to secondary heat transfer under degraded system conditions and into accident recovery techniques such as secondary feed and bleed to remove decay heat. LOFT tests provided also valuable data on thermal-hydraulics of fission product transport and coolant core melt interactions during reflood. Results of LOFT experiments stimulated many separate effect tests and code model development. LOFT data as from largest nuclear integral test facility form internationally the most important data base for assessment of system codes to predict nuclear power plant behaviour under accident conditions.

* Work supported in part by NRC and OECD-LOFT Program under the DOE Contract No. DE-AC07-76IDO1570.
1 Introduction

Safety of nuclear reactors can only be realistically assessed using computer codes that can capture the integral system behaviour over a complete spectrum of operations. Models for those codes are developed and assessed using separate effect tests. However, for validation of these codes large scale integral system tests are necessary. These tests allow one to assess the codes against data which are closest to the prototypic plant behaviour. Using integral test data for code assessment one can show the adequacy of the coupling of the individual components and process models over a complete range of accident conditions.

For many years, the LOFT facility played a principal role in reactor safety research providing data on Pressurized Water Reactors (PWRs) system behaviour during postulated accidents. Under the sponsorship of the U.S. Nuclear Regulatory Commission [1] and then later under the auspices of OECD, sponsored by a group of ten countries [2], forty four tests were conducted in LOFT between 1976 and 1985. Tables 1 and 2 list all LOFT experiments. The LOFT tests were a very significant source of broad range of data enhancing the understanding of system response phenomena and provided important thermal-hydraulic data for computer code and model assessment.

LOFT was the only source of PWR integral system data with a nuclear core. LOFT core thermal power was 50 MW, and its primary volume was about one fifteenth of typical 1200 MW(e) PWR [3]. The LOFT reactor was equipped with typical PWR safety systems. The major drawbacks of the LOFT facility were: one of its two loops did not have active steam generators or pumps, the elevations did not correspond to a commercial reactor system, and the core was shorter than typical PWR core. These LOFT design specifics introduced some distortions in LOFT transient response. However, distortions are inevitable in any scaled complex thermal-hydraulic system and should always be considered in evaluating the test data. The applicability of a facility for code assessment is given when the key thermal-hydraulic phenomena are controlled by the same governing properties as in the power plant. To assure that, the scale of integral facilities should be relatively large to reduce effects of nonprototypical heat losses, metal mass per fluid volume and leakages. These phenomena typically affect adversely simulated small break LOCA and long-term plant response. Also, particularly important is to provide for a scale (diameters) that will keep the friction loss per volume similar to those in the prototype. Small scale facilities have notoriously large surface area per volume affecting flow regimes and flow regime transitions resulting in nonprototypic behaviour. LOFT, because of its scale, large volume and large pipe diameters and also because of the nuclear core, certainly provided for conditions in that most of the two-phase phenomena were similar to those to be expected in commercial power plants. Therefore, LOFT generated the most important data for integral code assessment.

LOFT experiments simulated large break LOCA, small break LOCA, anticipated transients with and without scram, and some core damage and fission product release accidents. These experiments indicated the importance of a number of phenomena which were not shown earlier in separate effect experiments or even in smaller scale integral facilities experiments. To illustrate, in the following we discuss some of the most significant findings from LOFT experiments and indicate their significance and applications. A comprehensive review of LOFT test programs is provided in References 1 and 2.

2 Blowdown Core Cooling

One of the most important large scale integral test facility results were provided by the LOFT large-break LOCA experiments [4]. These tests, conducted during the US.NRC and OECD programs, showed for first time that there could be significant quenching of the cladding during blowdown which reduces the maximum peak cladding temperatures.

Two different thermal-hydraulic processes were responsible for these quenches. The first quench was as-
sociated with fluid moving up through the core and the second quench was result of liquid falling from the upper plenum onto the core. Analyses of the tests showed that the bottom-up quench is the function of the reactor coolant pump operation [4]. If the pumps are left running or if there is a long coast-down period, enough liquid will be delivered to the reactor vessel to cool the voided core. This cooling was found to be very efficient resulting in removal of most of the thermal energy stored in the fuel. The bottom-up quench reduced in the LOFT test the blowdown maximum cladding temperatures by as much as 200 K and accelerated the final reflood quench. Figure 1 shows a typical LOFT core thermal response during OECD LOFT Experiment LP-02-6. During this test a positive core flow at about 5 s was re-established resulting in quench of the lower two-thirds of the core by 10 s after break (Figure 1).

In transients with disconnected reactor coolant pump flywheels or with earlier pump trip this bottom-up quench did not occur but there was a top down quenching that involved about one-third of the core. Water from the steam generators and the pressurizer drained back early in the transient and caused this top-down quenching of the upper part of the core.

Both quenching processes were not shown in smaller scale integral facilities or separate effect test nor were they predicted using Appendix K calculation rules. These tests demonstrated that there was a larger safety margin in the peak cladding temperature during design basis accidents, contributed to approval of analytical best estimate methods for safety analyses (the evaluation model analyses prohibited rewet during the transition from full to reflood pressures in the blowdown period).

In reference 5, it has been identified that there are no mechanistic models available for the sudden quenching during blowdown. Safety codes currently apply an empirical heat transfer coefficient, based on transition to nucleate-boiling, as soon as a low-quality, high-velocity, upward core flow is predicted to occur. As indicated in the same reference, data base is also lacking needed for the detailed analysis of the mechanistic processes and heat-transfer coefficients involved in quenching during blowdown. Code calculations [4] of the LOFT experiment also indicate that this is not a very satisfactory procedure.

3 Small-Break LOCA Coolant Inventory

The major safety issue during small break LOCA is the loss of primary coolant inventory and the ability of the protection systems to detect this in time and to restore the water inventory. Phenomena that primarily affect the coolant inventory vary as a function of break size, break location, and plant design parameters. Also, because of the long time scales of such transients, operator interventions can be important factor in aggravating or ameliorating the accident consequences.

The TMI-2 accident showed that operation of the reactor coolant pumps could have an important effect on system behaviour and coolant inventory during the course of a small break LOCA accident. Critical is the timing of the pump trip, that will affect the amount of depleted coolant, and therefore the possibility of core uncover and fuel cladding heatup. Several LOFT experiments were designed to investigate this issue [1,2]. Two US NRC cold leg break experiments have shown that continued pump operation during the transient results in large coolant losses than for early pump trip (Figure 2). In the test with the early pump trip, stratified flow regime was established in the cold leg leading to early break uncover (the break was located at the midplane of the cold leg pipe) and therefore reduction of mass loss rate through the break. The minimum coolant inventory reached in this test was about 36% of the initial mass. However, in the test with pumps kept running, more mass was depleted through the break. The running pumps provide to the break region a two phase mixture which is of higher density than the fluid leaving the break in the case of tripped pumps. When the pumps were eventually tripped, the two phase flow through the core was interrupted and the core uncover was followed by significant cladding heatup. The coolant inventory at this time was about 16% of the initial mass with collapsed liquid level below the core.
Two OECD LOFT experiments addressed that same issue but for hot leg breaks. In the test with the early pump trip, as expected, a stratified flow was formed in the hot leg soon after the pump trip. After the break plane was uncovered, the coolant depletion was significantly reduced and eventually high pressure ECC injection exceeded the break mass flow rate and the primary system started to refill. The minimum coolant inventory resulted in a level just below the loop piping elevation keeping the core well covered with liquid through the transient. In a transient with late pump trip, the pump operation maintained initially fairly homogeneous distribution of the vapor through the system but then density gradient and finally stratified flow formed in hot leg leading to break uncover and reduction of coolant flow loss rate. These two hot leg break experiments showed that timing of the pump trip during a hot leg small break LOCA has only a small effect on the minimum coolant inventory (Figure 3) and does not significantly affect the risk of core uncover. Formation of stratified flow in the hot legs make these transients similar.

Code analyses of the four LOFT experiments showed that the flow regime maps and the branching models were inadequate for proper simulation of the key phenomena responsible for coolant inventory during the transients. These problems were not realized based on analyses of smaller scale facility tests.

4 Reflood Effects During Severe Accident Transient

The last LOFT experiment was designed to investigate fission product transport from a severely damaged core through the RCS to the environment in a interfacing systems LOCA [6]. During this experiment the primary system was voided almost completely and the core brought to rapid heatup. The maximum measured fuel temperatures were 2100 K (Figure 4). Postirradiation examination of the damaged fuel bundle [7] showed material formations consistent with temperatures in the range of 2800 K and localized regions above 3000 K. The postirradiation examination of the bundle and examination of the recorded thermal-hydraulic data resulted in a conclusion that most of the damage to the fuel occurred during the reflood portion of the experiment and was associated with rapid temperature excursion caused by enhanced metal-water reaction.

Some of the evidence was provided by the large amount of hydrogen found in the primary system. If the hydrogen would be produced during the initial heatup phase, a large fraction of it would escape through the break. However, during the reflood phase the primary system was isolated and the generated hydrogen could be captured. Also, the steam flow rates during the transient were assessed to be so low that they allowed only generation of about 40% of the discovered hydrogen. These findings were supported with postirradiation analyses of the bundle and examination some fuel centerline thermocouple readings and evaluation of temperatures measured at the upper-tie plate which showed that the peak temperatures were reached during reflood. The high temperatures in the bundle were the result of the reflood water reacting violently with remaining hot zircaloy. This exothermal reaction, releasing large amount of hydrogen (0.8 kg, 80% of the hydrogen generated during the test), elevated the temperatures within the bundle to a degree that the escaping hot gases caused some melting of the upper-tie plate. At the same time, the steam flow rates and temperatures in the bundle were sufficient to relocate fuel pellets and melts upward to the upper tie-plate. Assessment of the fission products releases indicated that most of the release from the fuel happened during the reflood when the minimum fuel temperatures were reached.

The results were unexpected and stimulated intensive model development to enhance code predictive capabilities. Also, following analyses of the TMI-2 data showed that 32% of the hydrogen generated during this accident could have been produced during the reflood phase. Quenching of degraded core following water injection is clearly important for accident management. A proper understanding of these processes is therefore necessary.
5 Conclusions

Safety assessment of nuclear reactors require an understanding of the full range of the potential behaviour. This cannot be achieved via experimentation only because of safety implications in experimental reactors and economic limits. Therefore, the safety assessment is conducted by computer modelling of the reactor behaviour under postulated accident conditions. The computer models, the codes, must be validated to assure that they adequately reflect typical plant behaviour. Complexity of two-phase flow phenomena requires that the code will be assessed not only using separate effect tests but also integral system data. LOFT, as source of integral data provided a very important service to the nuclear reactor safety community showing the importance of integral testing and identifying behaviour or phenomena not shown previously in separate effect tests or smaller scale integral tests. The three issues discussed above are prime examples of LOFT service. These data not only contributed to understanding of phenomena but directly had effects on licensing and analyses methodology.

Results of LOFT experiments stimulated many separate effect tests and code model development. LOFT data, as from largest nuclear integral test facility, form internationally the most important data base for assessment of system codes to predict nuclear power plant behaviour under accident conditions.

References

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<th>Description</th>
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<td>50% hot-leg-break LOCA</td>
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<td><strong>NRC L2 Series: Nuclear Large-Break LOCAs</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>L2-2</td>
<td>12.09.1978</td>
<td>100% cold-leg-break LOCA; maximum heat generation, 33 kWe</td>
</tr>
<tr>
<td>L2-3</td>
<td>05.12.1979</td>
<td>100% cold-leg-break LOCA; maximum heat generation, 39 kWe</td>
</tr>
<tr>
<td>L2-4</td>
<td>06.16.1981</td>
<td>100% cold-leg-break LOCA; maximum heat generation, 40 kWe, rapid pump cooldown</td>
</tr>
<tr>
<td><strong>NRC L3 Series: Small-Break LOCAs</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>L3-0</td>
<td>05.31.1979</td>
<td>Shaker open PORV from hot standby</td>
</tr>
<tr>
<td>L3-1</td>
<td>11.20.1979</td>
<td>10-cm (4-in.) cold-leg, noncommunicative-break LOCA</td>
</tr>
<tr>
<td>L3-2</td>
<td>02.07.1980</td>
<td>2.5-cm (1-in.) cold-leg, noncommunicative-break LOCA</td>
</tr>
<tr>
<td>L3-3</td>
<td>04.15.1981</td>
<td>PORV LOCA and recovery (initiated at the end of L3-1)</td>
</tr>
<tr>
<td>L3-5/L3-5A</td>
<td>09.29.1980</td>
<td>10-cm (4-in.) cold-leg, noncommunicative-break LOCA; pumps off</td>
</tr>
<tr>
<td>L3-6</td>
<td>12.10.1980</td>
<td>10-cm (4-in.) cold-leg, noncommunicative-break LOCA; pumps on</td>
</tr>
<tr>
<td>L3-7</td>
<td>06.20.1980</td>
<td>2.5-cm (1-in.) cold-leg, noncommunicative-break LOCA</td>
</tr>
<tr>
<td><strong>NRC L5 Series: Intermediate-Break LOCA</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>L5-1</td>
<td>09.24.1981</td>
<td>35.6-cm (14-in.) cold-leg, noncommunicative-break LOCA with degraded ECC</td>
</tr>
<tr>
<td><strong>NRC L6 Series: Anticipated Transients</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>L6-1</td>
<td>10.08.1980</td>
<td>Loss of steam level</td>
</tr>
<tr>
<td>L6-2</td>
<td>10.07.1980</td>
<td>Loss of forced convection</td>
</tr>
<tr>
<td>L6-3</td>
<td>10.05.1980</td>
<td>Excessive steam load</td>
</tr>
<tr>
<td>L6-4</td>
<td>05.29.1980</td>
<td>Loss-of-feedwater</td>
</tr>
<tr>
<td>L6-6A</td>
<td>04.19.1982</td>
<td>Inadvertent boron dilution; nominal recirculation flow</td>
</tr>
<tr>
<td>L6-6B</td>
<td>04.21.1982</td>
<td>Inadvertent boron dilution; doubled recirculation flow</td>
</tr>
<tr>
<td>L6-7</td>
<td>07.31.1981</td>
<td>Rapid secondary side induced cooldown</td>
</tr>
<tr>
<td>L6-EB1</td>
<td>08.29.1982</td>
<td>Slow control-rod withdrawal</td>
</tr>
<tr>
<td>L6-EB2</td>
<td>08.26.1982</td>
<td>Rapid control-rod withdrawal</td>
</tr>
<tr>
<td>L6-EC1</td>
<td>08.26.1982</td>
<td>Primary-pump-based, small-break LOCA recovery, low voidage</td>
</tr>
<tr>
<td>L6-EC2</td>
<td>08.29.1982</td>
<td>Primary feed and bleed small-break LOCA recovery</td>
</tr>
<tr>
<td>L6-EC3</td>
<td>08.29.1982</td>
<td>Primary-pump-based, small-break LOCA recovery, high voidage</td>
</tr>
<tr>
<td>L6-ED</td>
<td>08.31.1982</td>
<td>Slow natural-circulation cooldown</td>
</tr>
<tr>
<td><strong>NRC L8 Series: Severe Core Transients</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>L8-1</td>
<td>12.10.1981</td>
<td>Rapid core recovery and reflood (initiated at the end of L3-6)</td>
</tr>
<tr>
<td>L8-2</td>
<td>10.12.1981</td>
<td>35.6-cm (14-in.) cold-leg, noncommunicative-break LOCA with delayed ECC</td>
</tr>
<tr>
<td><strong>NRC L9 Series: Anticipated Transients with Multiple Failures</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>L9-1</td>
<td>04.15.1981</td>
<td>Loss-of-feedwater with delayed scram</td>
</tr>
<tr>
<td>L9-2</td>
<td>07.31.1981</td>
<td>Rapid natural circulation cooldown (initiated at the end of L6-7)</td>
</tr>
<tr>
<td>L9-3</td>
<td>04.07.1982</td>
<td>Loss-of-feedwater ATWS</td>
</tr>
<tr>
<td>L9-4</td>
<td>09.24.1982</td>
<td>Loss-of-offsite-power ATWS</td>
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</tbody>
</table>
Table 2: OECD LOFT experiment program.

<table>
<thead>
<tr>
<th>Experiment Identification</th>
<th>Date Conducted</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>LP-FW-1</td>
<td>02/20/1983</td>
<td>Loss-of-feedwater, primary feed and bleed recovery procedure</td>
</tr>
<tr>
<td>LP-SB-1</td>
<td>06/23/1983</td>
<td>Hot leg SB LOCA, early pump trip</td>
</tr>
<tr>
<td>LP-SB-2</td>
<td>07/14/1983</td>
<td>Hot leg SB LOCA, delayed pump trip</td>
</tr>
<tr>
<td>LP-SB-3</td>
<td>03/05/1984</td>
<td>Cold leg SB LOCA, core uncover, secondary feed and bleed recovery procedure, accumulator injection at low-pressure differential</td>
</tr>
<tr>
<td>LP-02-6</td>
<td>10/03/1983</td>
<td>200% large-break LOCA, US licensing case</td>
</tr>
<tr>
<td>LP-LB-1</td>
<td>02/03/1984</td>
<td>200% large-break LOCA UK, licensing case</td>
</tr>
<tr>
<td>LP-FP-1</td>
<td>12/19/1984</td>
<td>Gap fission product release, large-break LOCA, German licensing case</td>
</tr>
<tr>
<td>LP-FP-2</td>
<td>07/03/1985</td>
<td>Fission product release at high fuel temperatures (above 2100 K), V-sequence</td>
</tr>
</tbody>
</table>
Fig. 1: LOFT LP-02-6 Core Thermal Response.
Fig. 2: Primary Coolant Mass Inventory During LOFT Experiments L3-5 and L3-6.

Fig. 3: Primary Coolant Mass Inventory During LOFT Experiments LP-SB-1 and LP-SB-2.
Figure 4: The maximum measured Center Fuel Module (CFM) thermocouple data at 25.4 cm (10 in.), 68.6 cm (27 in.) and 106.7 cm (42 in.) elevations (Reference 6).
SESSION 3

ASSESSMENT OF UNCERTAINTIES IN CODE CALCULATIONS

Session Chairman:
O. Sandervag, Swedish Nuclear Power Inspectorate
Stockholm
SESSION 3

SUMMARY

O. Sandervag

The benefit of doing best estimate calculations with quantified uncertainties was acknowledged. The use of such information would be for licensing and probabilistic safety assessment. The objective is to quantify the uncertainty for a reactor calculation.

Four key aspects of code uncertainties quantification were reviewed in this session: the validation matrix activity which is the first requirement when, as the first step in code uncertainties evaluation, a systematic code assessment is developed; the methods themselves which are being developed for code uncertainties quantification; the user effects and the role of counterpart testing, especially on the scaling issue.

VALIDATION MATRIX:

It was recognized that a validation matrix agreed upon internationally for PWR and BWR thermalhydraulics codes had been developed. A validation matrix for separate effects is under development. The first matrix was issued nearly 5 years ago, and it was proposed that this should be updated. In this update, data from new experimental programs and new requirements from new reactor concepts would be included. It was noted that only a few plant transients were included. Sufficient detail for code assessment from such experiment was judged to be difficult to obtain. The high cost of making an assessment against plant data could also be an obstacle to the use of such data.

METHODS FOR CODE UNCERTAINTIES QUANTIFICATION:

The two British and German methods and the CSAU methodology developed by the USNRC were reviewed. The quantification of uncertainties is a complex task and requires great efforts. A major development task is to make the methods less resource demanding. The two European methods are quite similar; one is based more on engineering judgment and might therefore require less effort at the expense of the quality and quantity of information obtained. This method requires changes in the code in order to be able to investigate model sensitivities. The methods will be completed in a relatively near future when sufficient demonstration and testing have been performed.

Common to all methods was that the phenomena must be right before quantitative uncertainties are meaningful. Scaling and extrapolation from a small scale to full scale is still being discussed.

USER EFFECTS:

Considerable variations when using the same code on the same problem had been observed. This had been caused by selecting different nodalizations, time steps and code options. Plain input errors were also committed. In addition, engineering judgement had to be exercised with respect to system characteristics (e.g. heat losses), components and boundary conditions. In order to reduce such aspects, improved dialogue between experimentalists and analysts was recommended. The user effects should be a part of every ISP evaluation. Other remedies are better user guide-lines, training, discipline and quality assurance. In
some cases code improvements could help since differences occurred when the users tried
to circumvent code deficiencies. Presenting results with different requirements on
converged solutions can also cause differences.

COUNTERPART TESTING:
The scaling and value of counterpart testing is a controversial issue. It is the phenomena,
not the scenarios, which should be scaled. A prerequisite is that the same phenomena
occur at both small and full scales. It is the impression that the phenomena at full scale
are more 3D when the volume scale grows. The 1D codes have difficulty in predicting such
behaviour. One difficulty for comparisons is that it is impossible to impose the same
boundary conditions at different volume scales. It is in general not possible to assess the
validity of extrapolation in a simple fashion. The span of volume scaling is in general too
large. One has to have a reliable computer code to transform findings from one scale to
another. It is considered that comparison between code calculations and separate effects
test at full scale is the only practical way of assessing code performance. Comparisons with
plant data are very helpful but not sufficiently challenging with respect to accidental
conditions.
"CSNI Validation Matrix for PWR and BWR Codes"

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Abstract

To provide an international common basis for the assessment of thermal hydraulic system codes, the task was given by the CSNI Working Group 2 to the "Task Group on Thermal Hydraulic System Behaviour" to prepare an internationally agreed validation matrix. Matrices for PWRs with U-tube and once through steam generators as well as for BWRs, mainly based on integral tests, have been worked out. In addition a matrix for separate effects tests is in preparation.

1. INTRODUCTION

For the analyses of transients and loss-of-coolant accidents (LOCAs) in Light Water Reactors (LWRs) thermal-hydraulic computer codes have been developed over the last twenty-five years.

Starting with relative simple computer codes in the early 1970's, a continuous development of the codes has been performed with respect to a more realistic description of thermal hydraulic phenomena and a more detailed system representation.

At the beginning of the 1970's, codes for the analysis of large break LOCAs had been requested. The codes were based on the homogeneous equilibrium model, assuming equal velocities and temperatures of vapour and liquid phases. The next effort in code development were directed by the demand for the simulation of transients and small leak accidents. The implementation of new models allowed for the separation of vapour and liquid by gravity. The representation of primary and secondary side with control systems and balance of plant models were extended.

In the middle of the 1970's the development of a new generation of thermal-hydraulic codes were initiated to provide analytical tools for a more realistic simulation of LWR behaviour under transient and accident conditions. Thermal and mechanical non-equilibrium phenomena have been taken into account. The effects of non-condensables have been
considered. These codes allow the simulation of transients, the entire range of break sizes as well as beyond design basis accidents including accident management procedures with operator interventions.

Parallel to the development of the analytical tools a large variety of experimental programs have been executed to improve the understanding of thermal-hydraulic phenomena, to study system behaviour, and to provide the required data base for code development and code assessment.

A very high number of separate effects tests have been performed for the development and validation of code models. While in the 1970's the experiments were conducted mainly at small scale test facilities, in the 1980's more attention has been directed to scaling. For example, in 1986, the first tests at the test facility UPTF, an imitation of a four loop 1300 MWe PWR with upper plenum, downcomer and the main coolant pipes in full scale reactor geometry, were performed.

The overall results of the code calculations are assessed mainly by data from integral test facilities. While in the early 1970's the experiments were focused on large break issues, in the following, parallel to the advancement in code development, integral tests have been carried out to investigate LWR system behaviour during transients, small leaks and beyond design basis accidents.

In addition to the results of integral tests LWR plant data of transients or accidents are being used to assess the predictive capability of the codes.

2. CSNI VALIDATION MATRIX BASED ON INTEGRAL TESTS

Taking into account the enormous database provided by the experimental programs, the task was given by the CSNI Principal Working Group No. 2 (PWG 2) to formulate an internationally agreed validation matrix for the independent assessment of large thermal-hydraulic computer codes.

The matrix is focused mainly on integral system tests and operating data from power reactors, assuming that the developmental assessment of individual models has been satisfactorily completed by the code developer. Separate effects tests have been added only in cases where suitable integral system tests could not be found to address a particular phenomenon [1, 2, 3].

2.1 Structure of the Validation Matrix

To systematize the selection of tests for code assessment, so-called "Cross Reference Matrices" have been established in a first step. Based on these matrices well-balanced sets of experiments have been defined in a second step.
2.2 Cross Reference Matrices

In the Cross Reference Matrices the important physical phenomena which are believed to occur during the transient or LOCA, the experimental facilities suitable for reproducing these effects, and the test types of interest are listed. Based on the knowledge and experiences of the Task Group members, the relationships
- phenomenon versus test type,
- test facility versus phenomenon, and
- test type versus test facility
have been assessed for relevance and suitability.

For PWR codes four individual matrices have been prepared, differentiating between
- large breaks,
- small and intermediate breaks for PWRs with U-tube steam generators,
- small and intermediate breaks for PWRs with once-through steam generators, and
- transients.

The matrix for small and intermediate breaks in PWRs with once-through steam generators has been prepared to address in particular phenomena which are unique to this reactor type.

For BWR codes two individual matrices have been prepared, differentiating between
- loss-of-coolant accidents, and
- transients.

In the Figures 1 to 6 the Cross Reference Matrices, as published 1987 in the CSNI report 132, are shown.

The relationship phenomenon versus test type is rated at one of three levels:
- simulated resp. occurring: which means that the particular phenomenon is occurring in that kind of test (closed circle in the matrix),
- partially simulated respectively occurring: only some aspects of the phenomenon are occurring (open circle in the matrix),
- not occurring (dash in the matrix).

The relationship test facility versus phenomenon is rated at one of four levels:
- suitable for code assessment: which means that a facility is designed in such a way as to simulate the phenomenon assumed to occur in the plant and it is sufficiently instrumented to reveal the phenomenon (closed circle in the matrix),
- limited suitability: the same as above with problems due to imperfect scaling or insufficient instrumentation (open circle in the matrix),
- expected to be suitable: definition introduced in some cases to emphasize that new facilities address in particular phenomena not sufficiently covered by previous integral system test facilities. A conclusive comment could not be made at that time as the matrix has been published (x in the matrix).
The relationship test type versus test facility is rated at one of three levels:
- already performed or planned: the test type is useful for code assessment purposes (closed circle in the matrices),
- performed but of limited use: this kind of test has been performed in the facility, but has limited usefulness for code assessment purposes, due to poor scaling or to lack of instrumentation (open circle in the matrices),
- not performed or planned (blank).

2.3 Selection of individual tests

Based on the Cross Reference Matrices, well-balanced sets of tests were selected according to the criteria
- each phenomenon should be addressed in test facilities of different scale,
- all test types should be included.

In all cases, attempts were made to find plant results or integral system tests to address each of the phenomena of interest. Only in cases where suitable plant results or integral system tests could not be found have separate effects tests been selected. Where counterpart tests or near counterpart tests were identified between two or more facilities, they have been included in order to address questions relating to scaling and facility design compromises.

The selection of the tests was made on the basis of personal knowledge and experience of the participants of the committees. Only tests already executed or planned through 1984 resp. 1985 are considered in the matrices published 1987.

2.4 Extension of CSNI-Matrix based on Integral Tests

The CSNI Cross Reference Matrix for small and intermediate leaks in PWRs has been extended to include tests related to accident management for a non-degraded core in Fig. 7. Accident management procedures are an additional safety concept in case of hypothetical failures of different safety installations in order to prevent core melt. Additional test types have been included to address the specific phenomenological conditions related to AM. Included are the following test types:
- high pressure primary side feed and bleed
- low pressure primary side feed and bleed
- secondary side feed and bleed
- reactor coolant pump restart in a highly voided primary coolant system
- primary to secondary leak in steam generators with multiple failures.

The accumulator behaviour has been included as additional phenomenon relevant for AM test types. Condensation and evaporation during accumulator ECC water injection at small pressure differences between accumulator and primary coolant system may lead to intermittent ECC water injection with low efficiency.
2.5 Revision of CSNI Matrix

The test matrix and the selection of individual tests to be proposed for the validation of computer codes will be revised when this will be necessary. This allows to consider new test facilities (e.g. PACTEL), or possibly facilities to investigate new reactor concepts, in the future it should be considered if any changes are necessary to include special phenomena of new reactor designs. New tests representing new test types conducted in available test facilities may also be considered to be added or to replace tests which were already proposed but are judged to be less important for code validation compared with the new tests. Priority will be given to counterpart tests when individual tests are selected from the SPES, LSTF, BETHSY, LOBI and PKL test facilities.

3. CSNI VALIDATION MATRIX BASED ON SEPARATE EFFECTS TESTS

There are several reasons for the increased importance now placed on the comparison of codes with separate effects test (SET) data.

Firstly, it has been recognized that the development of individual code models often requires some iteration, and that a model, however well conceived, may need refinement as the range of applications is widened. To establish a firm need for the modification or further development of a model it is usually necessary to compare predictions with separate effects data rather than rely on inferences from integral test comparisons.

Secondly, there is the question of uncertainties in predictions of plant behaviour. A key issue concerning the application of best estimate codes to LOCA and transient calculations is quantitative code assessment. Quantitative code assessment is intended to allow predictions of nuclear power plant behaviour to be made with a well defined uncertainty. Most schemes for achieving this qualification of uncertainty rely on assigning uncertainties to the modelling by the code of individual phenomena, for instance by the determination of reasonable ranges which key model parameters can cover and still produce results consistent with data. This interest has placed a new emphasis on separate effects tests above that originally envisaged for model development.

The advantage of separate effects tests are
- clear boundary conditions
- measurement instrumentation can be chosen to study one particular phenomenon
- reduced possibility of compensating modelling errors during validation of computer codes
- more systematic evaluation of the accuracy of a code model across a wide range of conditions up to full reactor plant scale
- steady state rather than transient observations possible.

A further incentive to conduct separate effects tests in addition to those carried out in integral facilities is the difficulty encountered in scaling predictions of phenomena from integral test facilities (which of necessity are in some sense small scale) to plant applications. Where a phenomenon is known to be highly scale dependent and difficult to model mechanistically, there is a strong need for conducting separate effects tests at full scale. In ge-
neral it is desirable to have a considerable overlap of data from different facilities; success-
fully predicting data from different facilities provides some confirmation that a phenomenon
is well understood.

The main objective in producing the SET Validation Matrix is to identify the best available
sets of data for the assessment, validation and, finally, the improvement of code predic-
tions of the individual physical phenomena. While both integral test data and SET data are
appropriate for code validation and assessment, for model development and improvement
there should be a strong preference for SET data.

In total 67 thermal-hydraulic phenomena have been identified which should be addressed
by Separate Effects Tests (Table 1), and 187 facilities have been identified as potential
sources (Table 2). The test facilities differ from each other in geometrical dimensions, geo-
metrical configuration and operating capabilities or conditions. Therefore, the number of
facilities relevant to an individual phenomenon provides some indication of the range of
parameters within which a phenomenon has been investigated and experimental data gen-
erated.

A first report "Facility and Experiment Characteristics for OECD/NEA-CSNI SET Validation
Matrix" [4] will be issued containing a catalogue of the separate effects test facilities carried
out within the OECD member nations. Those facilities for which sufficient information was
supplied to produce an Information Sheet are marked "x". Only those facilities which suffi-
ciently are known to produce an Information Sheet, (113 facilities), have been considered
further in process of selecting suitable test data. An example of an Information Sheet is
shown in Table 3 for the 1/30, 1/15 and 1/5 VESSEL CREARE test facility.

This information was collected to enable the selection of appropriate sets of test data for
inclusion in the SET Validation Matrix.

A second report "CSNI Separate Effect Test (SET) Matrix for Thermal-Hydraulic Code Va-
lidation" [5] is prepared which will contain the characterization of phenomena (description
of phenomena, relevance to nuclear reactor safety, measurement ability and data base,
present state of knowledge - predictive capability), the facility cross reference matrix, the
selection of relevant parameter ranges and a list of individual recommended tests along-
side each phenomenon, in table form.

4. STORAGE OF TEST DATA OF CSNI MATRIX IN NEA DATABANK

Data sets from reactor transient simulation tests selected for the integral test code valida-
tion matrix are to be stored in a central location. It was agreed that the NEA (OECD Nu-
clear Energy Agency) Data Bank in view of its broad experience in handling of large
volumes of computer data would be an appropriate place to acquire, store and redistribute
the matrix test data.

Data of 45 experiments have been received from CEC Ispra, France, Japan, Sweden,
Switzerland and the USA, which are listed in Table 4. The remaining approximately 70 ex-
periments selected for the integral test code validation matrix, which are listed in Table 5,
are expected to be sent to the NEA Data Bank in the near future.
The release and dispatch procedure of the test data are:
- All requests for CCVM data are channelled through PWG2. Authors are notified of each request and confirm the release, before the data are dispatched. Special safeguards are provided to prevent that data are sent out accidentally without prior authorization.
- Input decks for code validation are only sent out if explicitly requested. Prior author confirmation must be obtained.
- Requesters should state the precise dispatch format, in which they wish to receive the material. User profiles may be generated which contain default format specifications.
- A report is automatically generated and distributed with each dispatch. This report contains:
  - request identification number
  - name and address of requester
  - rules and conditions of use (to be defined)
  - writing format
  - file descriptions
  - file map
  - list of included documents.

5. CONCLUSIONS

An internationally agreed validation matrix for PWR and BWR thermal-hydraulic system codes has been established. The matrix, published in 1987, is based mainly on integral tests and provides a well balanced basis for independent code assessment. The matrix will be updated after completion of the SET Matrix. New data of experimental programs, new requirements with respect to additional accident sequences, and new requirements resulting from new reactor concepts will be taken into account.

An internationally agreed validation matrix for separate effects tests is being prepared.

6. OUTLOOK

The development of methods for the quantification of uncertainties in plant calculations is a major task for the future [6]. This requires a determination of code uncertainties, which is based on a systematic code validation. The CSNI-Matrices are a necessary prerequisite to achieve such a systematic validation.
7. REFERENCES

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Proposal for the Formulation of a Validation Matrix
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CSNI Validation Matrix for PWR and BWR Thermal-Hydraulic System Codes

Facility and Experiment Characteristics for OECD/NEA-CSNI SET Validation
Matrix
Draft, to be published as OECD-NEA-CSNI Report

[5] CSNI Separate Effect Test (SET) Matrix For Thermal Hydraulics Code Validation,
Draft to be published as OECD-NEA-CSNI Report

Quantification of Code Uncertainties,
CSNI Specialist Meeting on Transient Two Phase Flow, Aix-en Provence,
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Table 1

LIST OF SET PHENOMENA

0 BASIC PHENOMENA
  0.1 Evaporation due to Depressurisation
  0.2 Evaporation due to Heat Input
  0.3 Condensation due to Pressurisation
  0.4 Condensation due to Heat Removal
  0.5 Interfacial Friction Vertical Flow
  0.6 Interfacial Friction Horizontal Flow
  0.7 Wall to Fluid Friction
  0.8 Pressure Drops at Geometrical Discontinuities
  0.9 Pressure Wave Propagation

1 CRITICAL FLOW
  1.1 Breaks
  1.2 Valves
  1.3 Pipes

2 PHASE SEPARATIONS/VERTICAL FLOW
  2.1 Pipes/Plena
  2.2 Core
  2.3 Downcomer

3 STRATIFICATION IN HORIZ. FLOW
  3.1 Pipes

4 PHASE SEPARATION AT BRANCHES
  4.1 Branches

5 ENTRAINMENT/DEENTRAINMENT
  5.1 Core
  5.2 Upper Plenum
  5.3 Downcomer
  5.4 SG-Tube
  5.5 SG-Mixing Chamber (PWR)
  5.6 Hot Leg with ECCI (PWR)

6 LIQUID-VAPOUR MIXING WITH CONDENSATION
6.1 Core
6.2 Downcomer
6.3 Upper Plenum
6.4 Lower Plenum
6.5 SG-Mixing Chamber (PWR)
6.6 ECCI in Hot and Cold Leg (PWR)

7 CONDENSATION IN STRATIFIED CONDITIONS
7.1 Pressuriser (PWR)
7.2 SG-Primary Side (PWR)
7.3 SG-Secondary Side (PWR)
7.4 Horizontal Pipes

8 SPRAY EFFECTS
8.1 Core (BWR)
8.2 Pressuriser (PWR)
8.3 OTSG Secondary Side (PWR)

9 CCF/CCFL
9.1 Upper Tie Plate
9.2 Channel Inlet Orifices (BWR)
9.3 Hot and Cold Leg
9.4 SG Tube (PWR)
9.5 Downcomer
9.6 Surge line (PWR)

10 GLOBAL MULTIDIMENSIONAL FLUID, TEMPERATURE, VOID AND FLOW DISTRIBUTION
10.1 Upper Plenum
10.2 Core
10.3 Downcomer
10.4 SG-Secondary Side

11 HEAT TRANSFER
11.1 Natural, forced convection: Core, SG, Structures
11.2 Subcooled/nucleate boiling: Core, SG, Structures
11.3 DNB/dryout: Core, SG, Structures
11.4 Post CHF heat transfer: Core, SG, Structures
11.5 Radiation: Core
11.6 Condensation: SG, Structures

12 QUENCH FRONT PROPAGATION/REWET
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<td>GUIDE TUBE FLASHING (BWR)</td>
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### Table 2: List of Separate Effects Test Facilities provided by OECD Member Countries

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Table 3: Example for Information Sheet
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Table 3a: Example for Information Sheet
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1 This is the OECD identification for test L2-6
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Figure 2: Cross reference matrix for small and intermediate leaks in PWRs
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</tr>
<tr>
<td>Precooling of water</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Surge tank hydraulic</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Coolant pump behaviour</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Structural stability of coolant vessels</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Noncondensable gas effects</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Phase transition in reactor and effect on maxim</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Intermediate two-phase natural circulation</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Natural circulation, vent valve, downcomer</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Superheating in secondary side</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

Figure 3: Cross reference matrix for small and intermediate leaks in PWRs with OTSG (in addition to the matrix for PWRs with UTSG)
<table>
<thead>
<tr>
<th>Phenomenon</th>
<th>Test Type</th>
<th>Test Facility System Tests</th>
</tr>
</thead>
<tbody>
<tr>
<td>Natural circulation in 1-phase flow</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
<tr>
<td>Natural circulation in 2-phase flow</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
<tr>
<td>Core thermomixtures</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
<tr>
<td>Thermomixtures on primary side of SG</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
<tr>
<td>Core thermomixtures</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
<tr>
<td>Pressurizer Thermomixtures</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
<tr>
<td>Primary inventory (COOL, COOLING)</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
<tr>
<td>Vars test flow</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
<tr>
<td>1 and 3 phase pump behaviour</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
<tr>
<td>Thermomixtures - nuclear - approach</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
<tr>
<td>Structural test and test cases</td>
<td>Test Facility System Tests</td>
<td></td>
</tr>
</tbody>
</table>

(a) volumetric scaling
(b) for phenomena requiring separate effects test, e.g., pressurizer behaviour, refer to small loss cross reference matrix
(c) flow behaviour will be strongly design-dependent, specific experimental data should be used if possible
(d) problem for scaled test facilities

Figure 4: Cross reference matrix for transients in PWRs
### Figure 5: Cross reference matrix for LOCA's in BWRs

<table>
<thead>
<tr>
<th>Test Type</th>
<th>System Tests</th>
<th>Separate Effects Tests</th>
</tr>
</thead>
<tbody>
<tr>
<td>Breakdown</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core Break without Depressurization</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core Break with Depressurization</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core Break without Depressurization and Hot Leg Break</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core Break with Depressurization and Hot Leg Break</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core Break without Depressurization and Hot Leg Break and Cold Leg Break</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core Break with Depressurization and Hot Leg Break and Cold Leg Break</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Cold Leg Break</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Hot Leg Break</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**Legend:**
- (a) Experiments performed in single bundle facilities at different power levels can supply valuable information for assessment of this phenomena
- (b) No two-phase data for full scale and jet pumps can be identified
- (c) No complexity sufficient data base could be identified
- (d) Data are provided by ESTIF for the upper plenum, and by UPTF for downcomer
- (e) Can be assessed using TH-data from PWR Veldhoven MAMO
- (f) These are non-LOCA data but may be used for assessment
- (g) Volumetric scaling
- (h) Overcooling not addressed
- (i) Structurally different from BWR

Phenomena included in this matrix may also be important for transects.
Figure 6: Cross reference matrix for transients in BWRs

<table>
<thead>
<tr>
<th>Phenomena</th>
<th>Test Type</th>
<th>System Tests</th>
<th>Sep. Eff. Tests</th>
</tr>
</thead>
<tbody>
<tr>
<td>Collapse of Level Behavior in Downcomer</td>
<td>Stationary Test</td>
<td>Mixing in Core</td>
<td>ROA-FB</td>
</tr>
<tr>
<td>Core Thermal Hydrodynamics</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Water Level Flow</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Single Phase Pump Behaviour</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Parallel Channel Effects</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Nuclear Thermal Hydraulics</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Reactor Thermal Hydraulics</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Reactor Thermal Instabilities</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Downcomer Mixing</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Boron Mixing and Distribution</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Steam Line Dynamics</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Void Collapse and Temp Distribution During Pressurization</td>
<td></td>
<td></td>
<td></td>
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<tr>
<td>Critical Power</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Level after RB of High Press and High Power and High Flow Core Flow</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Structural Heat and Heat Losses</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

(a) Referent data were identified for only one type of feedwater valve, one type of main isolation valve and check valve, one type of main isolation valve and one type of safety valve.

(b) Two-phase pump behaviour is of interest for certain special ATWS and inadvertent increase of steam flow transients.

(c) No data base could be identified.

(d) Volumetric scaling.

(e) Structurally different from BWR.

Phenomena included in LOCA reference matrix may be also important.
Figure 7: Cross reference matrix for small and intermediate leaks in PWRs extended to AM-test-types.
QUANTIFICATION OF CODE UNCERTAINTIES

H. Holmström, OECD/NEA

ABSTRACT

The assessment of so-called best-estimate (BE) codes aims at providing sufficient evidence that they can be used reliably and accurately enough within their intended ranges of application for realistic prediction of the behaviour of nuclear power plants during various accident and transient conditions. Until now the assessment has been essentially qualitative, with only minor exceptions. Only recently there has been increasing demand for quantification of the uncertainties, especially in conjunction with efforts to allow use of the BE codes (instead of the conservative "evaluation model" EM codes) in licensing analyses, and to achieve a corresponding reduction in the safety margins where considered excessive.

However, quantification of code uncertainties is an extremely complex task, and it has become evident that there will always be an element of subjective judgment involved.

This paper will focus on description and comparison of three major methodologies developed for code uncertainty quantification: one by AEA Technology, UK, another by GNS, Germany, and the third (CSAU) by USNRC, USA. The European methods are quite similar, but differ distinctly from the American method. While sensitivity studies for production of model parameter uncertainties are essential in the former, and comparisons with experimental data serve to check the validity of the results, comparisons of code predictions with integral test data are central to the latter. The role of subjective judgment is smallest in the GRS method.

1. INTRODUCTION

So-called best-estimate (BE) computer codes have been developed for realistic prediction of the thermal-hydraulic behaviour of nuclear power plants during various accident and transient conditions. This development has required an enormous effort, especially between years 1975–1985, and the codes are currently being assessed and applied widely in the nuclear community.

In the past, when the primary emphasis was on Large Break Loss of Coolant Accidents, all the codes had many conservative features and also the calculations were performed in a conservative way. Consequently, in the assessment it was adequate to prove that the key results (especially peak clad temperature, PCT) were always conservatively calculated.
Later, when Small Break Loss-of-Coolant Accidents and other transients gained more attention, the situation changed and became more complicated. Often it was not easy to determine if a feature or assumption was really conservative or not. It also depended on the purpose of the analysis. Sometimes the results of a conservative calculation were so unrealistic that they tended to confuse and mislead the analysts and the designers. It was difficult e.g. to prepare operator guidelines. It was also difficult to judge if one calculation or code was better than another and to decide how to improve the codes.

Mainly because of these reasons it was generally understood that in the future the main emphasis should be on the development and assessment of BE codes. Quantification of the uncertainties became necessary in conjunction with efforts to allow use of BE codes also in the licensing analyses in order to get more realistic predictions and achieve reductions in safety margins.

For all code assessment it is very important to identify the different sources of uncertainty in code predictions of experiments and especially of plant transients. Five main categories may be distinguished:

1) Specification of the problem
   - If the problem has been specified incorrectly in the beginning, the code may calculate irrelevant phenomena or quantities. The intended use of the results should be the basis for determining e.g. the scope of the problem.

2) Formulation of the conceptual model
   - A code is based on a conceptual model, which already includes selections and simplifications of real physical processes, e.g. the number and form of the conservation equations, correlations, special models etc.

3) Formulation of the computational model
   - A computational model consists of a code and its geometric model (nodalization). A code may contain programming errors, the numerical integration scheme may not be completely stable and convergent, numerical diffusion and truncation errors always exist and the nodalization does not represent the details of the geometry.

4) Determination of the model parameters
   - Often model parameters have large margins of uncertainty and are "tuned" during the developmental assessment.

5) Calculation and documentation
   - The major source of uncertainty associated with the calculation is the input deck. In addition to mistakes like typing errors there are uncertainties in initial and boundary conditions and selections of options (models, correlations). The "user uncertainty" can be reduced e.g. by improving user guidelines. It is, of course, also important that the final documentation is complete and correct.
Code assessment concentrates in comparison between the results of code calculations and measured experimental or actual plant transient data. However, it should also include other type of studies of the above mentioned sources of uncertainty.

Traditionally code assessment has been divided into two main types:

1) Developmental assessment
2) Independent assessment

The former is performed by the developers of the code during the development in order to assess the separate models or parts of the code and to help in selecting and developing the models. Some calculations are also done with the whole code to check if all the parts function together as intended. The latter is carried out by other (independent) users in order to find out if the code is reliable and accurate enough when used as intended.

The independent assessment may include the following steps.

1) Qualitative assessment
2) Quantitative assessment
3) Extrapolation to full size plants

Qualitative assessment focuses on the overall impression of the code's capabilities, especially correct simulation of all the relevant phenomena. Quantitative assessment aims at expressing the accuracies or uncertainties as numbers. The third step (scaling capability) is the final step for arriving at a quantified statement of the uncertainties associated with predictions which full scale NPP plant accidents and transients can be predicted.

There are several problems associated with any type of assessment of the codes. Some of the most important ones are discussed below.

There are basic problems associated with experimental data. The CSNI Validation Matrices contain good selections of available data, but the entire database is still limited both in quantity and in quality. The cross reference matrices, however, give useful indications of data which are lacking (phenomena, scale). But even if an experiment seems to be "ideal" for investigating certain phenomena, a closer look may reveal e.g. the insufficiency of the instrumentation, failures of instruments and/or too large or unknown uncertainties. Often experimental data reports are incomplete (missing facility data, incomplete initial/boundary condition information, some out-of-date information etc.)

Another basic problem is connected to the so-called "freezing" of the code. A code, which is undergoing continuous development, cannot be used reliably nor can it be assessed properly. A certain code version to be assessed should first be "frozen" (kept unchanged) and documented. Thus, all users would have the same code version, and assessment results and user experiences could be exchanged knowing that the results are inter-related. However, during the assessment it is very likely that programming errors and deficiencies are detected and some models are found to be
unsatisfactory. It has become a common practice that minor error corrections or additions of user convenient features are allowed during the assessment, but other changes are not. If, nevertheless, serious deficiencies are found, completing an extensive assessment task would mean a waste of time and resources, because a defective code, even if assessed, is probably useless. So the whole assessment process should be repeated after correcting the code. One possibility to avoid this waste is to calculate first only a small number of best available test cases, and to continue only if they have been completed satisfactorily. Another possibility would be to stop the assessment work immediately after a major deficiency is reported to be found.

So-called "code tuning" may also cause problems. This means that input parameters, user options and/or nodalizations are adjusted, and even models changed, in order to get the best possible agreement with certain test data. This is unacceptable. If a new set of "optimum" parameters is defined for every new test case without sound technical justification, the assessors are misled. The objective of a code assessment calculation is not to get the best possible results, but to obtain objective information of the accuracy of the code when used according to the normal user guidelines.

It should be also noted that the communication channels between the code developers and users (assessors) should be open and efficiently used in both directions. Fast and well-documented feedback from the users is extremely important.

Finally, the following questions should be answered: How good is good enough? How should the code acceptance criteria be defined? When should code development stop if ever? If a code has been assessed qualitatively, the only option to answer the first two questions is in a qualitative, subjective way based on experts' opinions. The regulatory requirements and realistic achievability serve as boundary conditions. The codes cannot be evaluated against criteria more restrictive than one's understanding of the phenomena. The criteria also depend on the application. With the help of sensitivity studies, uncertainties can often be handled satisfactorily. As to the last question, it could be discussed and answered during this meeting.

2. QUALITATIVE ASSESSMENT

Qualitative assessment, as already mentioned, focuses on the overall impression of the code's capabilities. The code is compared against a wide variety of experiments, mainly integral, to check if the trends of the data are correctly predicted. Of course the accuracy should also be reasonable. The comparison is based on subjective engineering judgment. Little or no attempt is made to quantify the accuracy or uncertainty at this stage.

This type of assessment has been virtually the only type that has been applied so far. The work, especially in the main code developing countries like the United States, has been extensive. Qualitative assessment experiences have been reported to safety authorities, code developers
and other users. The former have followed the assessment work closely to make sure that the codes can be relied on. The latter have learned about the limitations of the codes and improved their skill in using them. The code developers have continuously improved the codes based on the assessment results. Licensing applications have still resorted to conservative assumptions and parameter sensitivity studies.

The NEA/CSNI Principal Working Group No. 2 (PWG2) (and its predecessor) has been active in the assessment field already since 1974, when it was agreed to start the so called International Standard Problem (ISP) program, which turned out to be a success and it is still continuing.

An ISP means an experiment (or an actual transient in a nuclear power plant), which is calculated in the participating countries with different computer codes /1/. The performer of the experiment usually acts as the host organisation. The ISPs have formed an important part of an assessment effort, but they have also very effectively served other purposes, like learning how to use the codes in a better way with greater confidence, comparing different codes, studying the effect of the user, improving understanding of the transients, providing information of the safety margins and suggesting necessary improvements to the codes.

In addition to the ISP program, the NEA/CSNI/PWG2 "Thermal-Hydraulic" Task Group has been quite active during the recent years in reviewing the whole assessment process and preparing lists of required test cases. These lists have been based on the so-called Cross Reference Matrices, which were first prepared to systematize the selection. Accordingly the lists were called Validation Matrices /2/. The matrices are being updated and supplemented on a continuous basis.

The Task Group has also formulated a recommended procedure for qualitative assessment. It includes the following steps:

1) Establishment of the transient categories for which the code is to be assessed (transients-single/two-phase, small breaks, large breaks-blowdown/refill/reflood,...)
2) Selection of the test cases (use of the CSNI Validation Matrix)
3) Specification of transient phases, which are treated separately ("phenomenological window" = time span in which the same phenomena and parameters dominate)
4) Specification of important phenomena (use of the CSNI Validation Matrix)
5) Selection of the parameters to address the important phenomena (e.g. core quench - cladding temperatures, core fluid dynamics)
6) Calculation of the test cases
7) Comparison of the calculated and experimental data
   - stable solution?
   - important phenomena simulated reasonably well?
   - phenomena coupled correctly? (compensating errors?)
   - reasonable time comparisons?
   - do "threshold" phenomena require special attention? (e.g. after dryout there is a drastic change in the cladding temperature)
8) Sensitivity studies if needed (to investigate special problems, e.g. effects of "threshold" phenomena)
9) A (first) judgment on whether the code can be used in full scale LWR analyses with sufficient confidence

If it is the intention to perform the quantitative assessment phase afterwards, the qualitative assessment phase may include preparations for the quantitative phase to make sure that the results of both of the assessment phases will be consistent. At the end of the qualitative phase it should also be considered whether it is worth while to perform the quantitative phase at all. For example, if the results already show that the accuracy of the code is clearly unacceptable, there may not be a need to quantify this (in)accuracy.

3. QUANTITATIVE ASSESSMENT

As stated earlier, if BE codes are to be used in licensing analyses, the accuracy of the analyses must be known particularly well. This means a need for quantification. Quantitative assessment aims at expressing the accuracies and/or uncertainties as numbers. In fact, these numbers should refer to full size plants, and not only represent the code's ability to calculate a selected set of experiments.

Concerning the words "accuracy" and "uncertainty" the CSNI FWG2 Task Group on Codes has agreed on the following definitions (figure 1):

- Code uncertainty band with respect to a certain output parameter includes the set of outcomes with probabilities larger than a given number.
- Code accuracy is the difference between corresponding calculated and measured parameter values or a measure of this difference.

The Task Group has mainly considered the following three different quantification methods. One has been developed by the AEA Technology, Winfrith, one by the GRS, Manich and one by the USNRC (Code Scaling, Applicability and Uncertainty Evaluation Method, i.e. CSAU Method). In addition there have been proposals that aim at improvement of these methods. In fact, all of these methods are incomplete (except CSAU) and under continuous development. They have not yet been fully tested. All three of them are first described separately below and then compared with each other.

3.1 The Winfrith Method (AEANM)

In the first of the methods ("Winfrith Method", AEANM) the aim is to extend the current qualitative method to include some meaningful quantitative assessment of uncertainty. Mechanical (and expensive) application of statistical techniques are claimed not to be justified by the nature of the problem and the data available. The method relies heavily on experts' judgment and experience, and attempts to use them in a logical way. It is designed to generate useful information with the minimum of effort. The main idea is the use of parameter sensitivity studies to generate code uncertainty bands for the key output parameters. The choice of the model parameters (and boundary conditions) as well as the re-
quired number of sensitivity calculations is based on experts' judgment. Comparisons to the measured data of selected integral tests serve to check if the code accuracy is acceptable (uncertainty bands bracket the data and are not so large that the results are useless). The same procedure is applied to both integral tests and plant transients. It is argued that the generated uncertainty bands for the latter are meaningful if the procedure has been applied successfully for the former.

Figure 2 shows a flowchart depicting the Winfrith method. It, in fact, represents the whole process of code assessment from code development to final code application. The first three steps are consistent with the current practice.

There are some requirements of the code before the assessment method (from step 4) should be applied, e.g.:

- Numerically stable, reliable
- Absence of non-physical discontinuities
- Reasonably fast-running
- All relevant physical phenomena addressed
- Constituent models can be biased to predict the phenomena reasonably accurately

The last requirement addresses the need for facilities in the BE codes to carry out sensitivity studies. If model parameter variations are not possible through input data, the code will have to be modified, recompiled and relinked for each sensitivity study, which increases the amount of work and the scope for error.

As was already mentioned, uncertainty bands are derived in the same way for integral experiments and plant transients. In both cases it is important to understand the transient as well as the code under scrutiny to ensure that all important phenomena and the corresponding code models are identified. The next step is the choice of the model parameters to be varied and their "reasonable uncertainty ranges" (excluding the values, which seem to be inconsistent with reasonable certainty), which are based on experimental data from separate effects tests. A set of variant calculations (sensitivity studies) can then be carried out to identify reasonable bounds in chosen output variables (key parameters). This does not require a separate calculation for every parameter variation. Where the expected effects of several variations is in the same direction these may be combined in a single run. The resulting bounding curves of the output parameters vs time may be determined most simply by taking the maximum and minimum of the calculated values at any time, though in some cases alternative approaches may be desirable.

Comparisons to the measured data of integral tests serve to check if the method and the generated uncertainty bands are valid. If the data are not bracketed by the bands, it means a failure of the approach and has to be corrected in a justifiable way (e.g. correct or improve the code). Furthermore it is not sufficient only to bracket the data; the bands must also be narrow enough to be useful. Blind standard problems are especially valuable for testing the method. Only if the method has been shown to be successful in the case of integral tests, can it be applied
to plant transients with sufficient confidence.

The first feasibility study on this assessment method using the LOBI small break LOCA test BL-02 as the test case has been performed /3/.

The main objectives of the study were the following:

- To find out the number of variant studies required
- To investigate the usability and adequacy of the separate effects database for determining the required model parameter uncertainty ranges
- To study the difficulties in performing the model parameter variations in the absence of a facility to access them through input
- To check the adequacy of the experts' initial judgment on the key phenomena and parameter uncertainty ranges
- To examine the special problems of computational model implementation and noding uncertainties
- To find out the overall effort required to apply the method

A limited number of parameters was considered. Some problems were encountered when modifying TRAC to allow for performing the sensitivity studies. The measured (single-valued) minimum pressure difference across the core, the time of its occurrence and minimum primary inventory were bounded by the calculated uncertainty bands, the continuos-valued core pressure difference and primary inventory were mostly bounded, and the time of loop seal clearance was not bounded. The general conclusion of this so-called "pilot study" was that the method meets the basic requirements for an uncertainty evaluation method, and its development should be continued, as is presently being done.

3.2 The GRS Method (GRSM)

In a second method ("GRS Method", GRSM) the aim is to reduce the amount of experts' judgment to a practical minimum. The main idea is the use of probability distributions to express the uncertainties in the computational model parameters and boundary conditions, and to propagate them through the code (performing a number of calculations) to generate probability distributions of desired output parameters. This is done in the same way for both plant transient and integral experiment analyses.

Figure 3 shows a flowchart of the central elements of the GRSM. The main steps of the method are:

1) Selection of transient category
2) Specification of phenomenological window (e.g. reflood)
3) Selection of characteristic integral experiment (IT)
4) Determination of dominant phenomena (DP) in the IT
5) Determination of models describing the DP
6) Determination of uncertain model parameters (MPS) contributing to the uncertainty of the prediction of the IT
7) Determination of uncertain boundary conditions (BCs) contributing to the uncertainty of the prediction of the IT
8) Specification of the maximum conceivable uncertainty ranges of the
model parameters and corresponding probability distributions (based on experts' judgment) taking into account correlations between the model parameters

9) Specification of the maximum conceivable uncertainty ranges of the boundary conditions and corresponding probability distributions (based on experts' judgment)

10) Specification of the key output parameters

11) Setting up a joint subjective probability density function (joint pdf) for the combined range of the parameter values, and propagation of the joint pdf through the model via random sampling and performing a number of calculations of the case to generate a pdf of the output parameters (key parameters)

12) Derivation of quantitative statements about the effect of the (input) parameter uncertainties on the prediction

13) Ranking of the parameters with respect to their contribution to the overall uncertainty of the prediction

14) Comparison of the predictions to the measured data to check if the data are bracketed by the uncertainties derived at step 7

15) Repetition of steps 1 to 14 for a number of integral experiments to check if the measured data are consistently bracketed by the generated uncertainties

16) Application (steps 1 to 13) of the method to plant transients

Experts' judgment at steps 8 and 9 is based on calculations of a number of most suitable separate effects (SE) experiments (taking into account e.g. scaling effects). Full size SE tests and real plant transients are utilized as much as possible in the process. Additional uncertainties are still likely to be used. As to step 11 it is argued that sufficiently good estimates (95% confidence that they are over-estimates) of the 95% fractiles of the subjective probability distributions of the output parameters can be obtained with 59 runs (per experiment or plant transient), regardless of the number of (input) parameters.

Blind standard problems are considered valuable for testing the method.

The first feasibility study of this method has been carried out. The main objectives of the study were:

- To study the difficulties in specifying the probability distributions of the (model and boundary condition) parameters
- To investigate the possibilities to account for correlations between the parameters
- To study the difficulties in performing the model parameter variations (as with the EM method)
- To find out the overall effort likely to be required to apply the method

A new project has just been started in cooperation with the French CEA to apply the method to the analysis of an OMEGA Test with the ATHLET code. Its objectives include further studies on the determination of the model parameter (MP) and boundary condition (BC) ranges and distributions, on the dependencies between the parameters and on the overall effort involved. The user effect on code results is also being addressed to some extent.
3.3 The USNRC Method (CSAU)

In a third method (The USNRC's CSAU Methodology) the main aim is to combine quantitative analyses and expert opinions to arrive at a computed value of the expected uncertainty in code predictions of any specific plant transient using mainly code-data comparisons from a selected set of assessment cases (integral and separate effects experiments). The whole methodology, which addresses all the aspects of code assessment (qualitative and quantitative, scaling capability and applicability to given scenarios) is called the Code Scaling, Applicability and Uncertainty (CSAU) Evaluation Methodology.

The method /4/ is being developed by the USNRC and its contractors and consultants. Its stated objectives are:

1) To provide a technical basis for quantifying uncertainties within the context of the revised ECCS Acceptance Criteria,

2) To provide an auditable, traceable and practical method for combining quantitative analyses and expert opinion to arrive at computed values of uncertainty, and

3) Provide a systematic and comprehensive approach for:
   a) defining scenario phenomena
   b) evaluating code applicability
   c) assessing code scale-up capabilities, and
   d) quantifying code uncertainties concerned with:
      - code and experiment accuracies
      - code scale-up capabilities
      - plant state and operating conditions

It has been concluded that in order to make the method practical, it must be made simple and easy to use. That is why it focuses only on important phenomena and resorts relatively much to experts' judgment. The CSAU evaluation methodology consists of three primary elements as shown in figure 4.

The first element (Requirements and Code Capabilities) contains steps 1 to 6:

1) Specification of the scenario to be analysed in order to establish the set of parameters to be evaluated.

2) Selection of Nuclear Power Plant to further define the scenario.

3) Division of the scenario into operationally characteristic time periods (windows), if necessary, and identification and ranking of important physical processes in order to establish Process Identification and Ranking Tables (PIRTs) appropriate to the particular time periods of the scenario. These are used to select the significant processes for detailed examination. A so called Analytical Hierarchical Process (AHP) is one available ranking method.

4) Selection of code version, and "freezing" it in order to ensure that changes to the code do not impact the conclusions of the assessment.

5) Provision of complete code documentation, which includes the code manual, the user guide, the model/correlations quality evaluation (MC/QE) document, and other relevant supporting documents. The MC/QE is requested for each BE code by the NRC. It should provide detailed
information of the closure relations as implemented in the code
(sources, data bases, accuracies, scale-up capabilities, etc.)
6) By comparing the requirements (steps 1 to 3) and the code capabili-
ties (step 5) tentative determination of the applicability of the
code to the analysis of the scenario. Checking whether the capabi-
lities are adequate to model the important processes.

The second element (Assessment and Ranging of Parameters) contains steps
7 to 10:

7) Establishing an Assessment Matrix for assessing the accuracy of the
code to calculate the dominant processes and phenomena identified in
the PIKs and to address any code inadequacies identified in step 6.
The Matrix should contain both Separate Effects and Integral Tests
(SETs and IITs).
8) Definition of the Nuclear Power Plant (NPP) nodalization. It should
be detailed enough to capture the important phenomena and design
characteristics of the NPP. The same nodalization should be used in
performing both the NPP and the code assessment calculations.
9) Calculation of the tests in the Assessment Matrix, and determination
of the accuracy of the code calculations by quantifying the differences
between the code results and the test data for bias and uncer-
tainty (deviation), taking into account the uncertainties in the ex-
perimental data. Large scale SET data should be used to evaluate the
scale-up capabilities of the code and the accuracy to model important
phenomena, whereas IT data should be used to evaluate the overall
code accuracy. Also the ranges of parameter variations needed for
sensitivity studies must be specified.
10) Determination of the effect of scale. Quantification of the differ-
ences for the potentially scale dependent physical processes at dif-
f erent scales (bias and deviation), and (using judgment) establishing
an overall statement of the uncertainty associated with the effect of
scale.

The third and last element (Sensitivity and Uncertainty Analyses) con-
tains steps 11 to 14:

11) Determination of the effect of the uncertainty in the reactor state
and operating conditions at the initiation of the transient. Realistic
variations are determined using statistical and experimental
data, as well as analytical studies.
12) Performing NPP sensitivity studies. The individual variabilities
cast in terms of bias and distribution are input to the NPP model
and used (varied) to determine their effect on the most important
output parameters.
13) Combination of all the biases and uncertainties determined in the
above steps in a statement of total uncertainty using a justified
combination method. Additional margins should be added, if warranted
(e.g. limitations in the data base. Total mean and 95% probability
values of the appropriate parameters should be produced.
14) Preparation of a statement of the total uncertainty for the code. It
may be an error band or statement of confidence about the code cal-
culation with respect to the primary safety criteria (e.g. PCT du-
ing a large break LOCA).
The CSAU methodology has been tested with one large break and one small break LOCA case in a PWR (latter of BWR design). During the testing the method was, in fact, developed to its current form. Several different approaches as to the details were explored, some of which proved fruitful and some did not. The first test case concentrated on determining the uncertainty in calculating the blowdown and reflood peak cladding temperatures, which are single-valued parameters. Extension to small break LOCAs and other transients requires development of a method to evaluate the uncertainties concerned with continuous-valued (continuous functions of time) parameters like water inventories and mass flows. Information on the small break LOCA test case has been very limited so far, but more is expected soon.

3.4 Comparison of the Assessment Methods

The two European assessment methods outlined above are very similar. The GRS Method differs from the Winfrith Method clearly only in its attempt to further reduce the amount of experts' judgment needed during the procedure. In the AEAAM the choice of the model parameters and the boundary conditions to be varied in the sensitivity analyses is based on experts' judgment, and the resulting uncertainty bands may be determined simply by taking the maximum and minimum of the output parameter values at any time (if considered appropriate). The GRSM, instead, aims at using probability distributions wherever feasible for expressing the uncertainties both in the model parameters and boundary conditions as well as in the most important output parameters. Otherwise the differences between the two methods are very small and may even be reduced during their further development.

The third method (CSAU) is distinctly different compared to the others. While sensitivity studies with parameter variations are essential in the latter and comparisons to test data only serve to check if the generated uncertainty bands are valid, the code-data comparisons are central in the CSAU Method. The determination of the uncertainties (accuracies) in that method is for the most important part based on direct comparison of the measured and calculated values of the key parameters from the tests of the selected Test Matrices.

Figure 5 shows two simplified flowcharts side by side in order to make the comparison of the European (FM and GM) and American (CSAU) methods easier.

3.5 Effect of scale

Generation of uncertainty bands around the predictions of hypothetical accidents and transients in large nuclear power plants is the final goal of all the assessment methods. However, the codes are mainly being assessed against relatively small scale experimental data. The number of full scale (or near) test facilities is very small, all of them of separate effects type, and the scarce plant data have only a supplementary role in assessment. That is why the effect of the incomplete coverage of the scaling range of the facilities on the final uncertainties is an im-
important issue in the assessment process. Scaling distortions of the fa-
cilities also have an indirect effect.

As the quantification methods are still under development, also the eva-
ulation of the scaling capabilities of the codes as the final step in
the process is still under scrutiny. Only in conjunction of the testing
of the CSAU methodology it has already been addressed in some detail.

In the CSAU Method, it is first important to note that ranking of the
processes and phenomena is exercised throughout the process. This means
that also scaling is addressed only where it is considered to be impor-
tant. The first studies of the effect of scale and its importance are
based on theory and documents. Then the applicable experimental data
base is investigated to find out, how well it covers the required sca-
ling range. All the (important) phenomena are studied one by one using
engineering judgment. Finally, if the theoretical and experimental data
base is considered insufficient in some cases, conservative assumptions
must be resorted to. Figure 6 shows a flowchart of the CSAU procedure to
evaluate the code scale-up capability.

As to the European methods, both of them emphasize the use of large
scale separate effects tests. Only if the physical processes are under-
stood very well theoretically, full scale SE experiments are not requi-
red. In connection with the development of the GRS Method, the processes
have been divided in three categories:

1) Only small scale SETs available, but effect of scale well enough
known theoretically.
2) Only small scale SETs available. Scale up capability of the code
not fully justified theoretically.
3) Full scale SETs available.

It is concluded that full scale tests are required in the second case,
but not in the first. In the third case it is reminded, that also small
tests must be analysed to check for the scale down capability of
the code.

The basic approach to the scaling uncertainty issue of all the methods
seems to be similar. It is probable, that the already small differences
will even decrease in the future, because the means to address the prob-
lem are very limited in number.

3.5 Difficulties and Problems

One basic problem in an effort to quantify the uncertainties associated
with code predictions is the lack of an ideal set of experiments to be
used in the assessment. The instrumentations of the test facilities are
not ideal nor do they perform perfectly. It is also very difficult to
quantify the effect of missing data.

Furthermore, even if the experimental data base was perfect, many prob-
lems and difficulties in applying quantification methodologies would
remain, as illustrated below.
All of the uncertainty quantification methods include comparisons between the calculated results and measured data, although it constitutes a more central part in the CSAU method. If the comparison concerns continuous-valued parameters, it seems to be very difficult to develop a defensible simple mathematical method to derive quantitative code accuracy statements, which are always consistent with experts' subjective judgment. For example, how to compare quantitatively, if timing of the phenomena in the test and in the calculation is different (fig. 7), or in the case of heavy oscillations (fig. 8), or if a dryout (example of a threshold phenomenon) occurred in the test, but not in the calculation (although it was very close) (fig. 9). Is it meaningful to compare parameters of a certain time period at all, if there is a substantial difference between the test and calculation already at the beginning of the period (fig. 10, refill period as an example).

As already mentioned the uncertainty quantification methodologies are still under development (except for CSAU?). In every method there are specific issues and potential problems, which, of course, are being and will be addressed during the development and testing phases of the methods. Some of them are listed below.

**Generic:**

- No ideal set of experiments exists (mathematical rigour requires "ideal" data)
- Are all sources of uncertainty addressed? (User effect?)
- Effect of scale is difficult to quantify accurately
- Experts’ judgment is being relied on
- It is difficult to test the methods
- Some input uncertainties are difficult to determine
- Application requires a large effort
- Continuous-valued parameters are difficult to compare (see above)

**AEAM and GRSM:**

- The codes must be modified so that the computational model parameters can be changed through input.
- It may turn out that some of the resulting uncertainty bands are too wide or the comparison to test data is unacceptable.

**AEAM:**

- Does excessive use of experts’ subjective judgment prevent production of meaningful quantitative uncertainty statements?
- Criteria for "narrow enough" uncertainty bands must be defined.
- Number of required calculations determined by the expert

**GRSM:**

- Number of assessment cases to be calculated to check if the data are satisfactorily bracketed by the generated uncertainties has not been specified. Use of the CSNI Validation Matrices unclear so far.
- Method to generate the probability distributions of the model parameters has not been defined yet.
- Selection of the parameters to be varied is unclear.
- The sampling technique to be used in selecting parameter value tuples has not been specified so far.

CSAU:
- It is very difficult to validate the method fully.
- The number of calculations to produce meaningful results has not been specified.
- Treatment of continuous-valued parameters has not been specified so far (special problems listed above).
- Adequacy of the method to combine the various uncertainty components is unclear.
- It is unclear whether the mathematical/statistical rigour of the method is adequate.

4. CONCLUSIONS

As stated earlier the two European Methods are quite similar. The AEWEM relies more on experts’ judgment and may require less effort, but at the expense of quality and quantity of the resulting information. The GRSM provides probability distributions of the output parameters plus other useful information, but the large effort involved may prevent its wide application. The CSAU Methodology also requires a lot of effort, and, in the author’s understanding, there are still some limitations in its range of application. However, it might be possible to develop all of the methods towards practical assessment tools, if supporting data bases and clear detailed guidelines will be developed. It also seems worthwhile to investigate possibilities to use the information and data generated while developing these methods for somewhat different purposes. For example, the application and testing of the methods has probably already revealed weaknesses and shortages in the available data base, which information could be used in the planning of future research strategies.

In the end any method will only be useful, if it can produce necessary results (information) with a reasonable effort. The applicability of the methods described will be determined in the near future after their development and testing will be completed.

REFERENCES

/1/ CSNI Report No. 17, Revision 3 "CSNI Standard Problems Procedures" OECD/NEA/CSNI, November 1989

/2/ CSNI Report No. 132 "CSNI Code Validation Matrix of Thermo-Hydraulic Codes for LWR LOCA and Transients" OECD/NEA/CSNI, March 1987


The Situation at Time $t_i$:

A single TRUE VALUE for $X$;

A single BEST-ESTIMATE prediction for $X$ plus values from uncertainty analysis parameter variations;

Several MEASUREMENTS $M_j$ of $X$.

The measurements are either subject to measurement error only, or to measurement error and variability within the control volume used in the code. Computed and true values of $X$ are to be the mean value over this control volume.

Accuracy Statement:

This requires a specification of the tolerable error. The code prediction $X$ is "accurate enough" at a confidence level of 90% if the interval about $X$, defined by the tolerable error, contains a 90% confidence interval for the true value. In the above situation, the true value is the mean value over the control volume. A confidence interval is derived from a random sample of measurements (corrected for bias) from this control volume. Note that 90% is here used for illustration, usually confidence levels of 95% or larger are employed.

If expert judgement is used to account for imperfections in experiments and for measurement error contributions to the uncertainty, confidence levels and intervals are to be called "subjective".

FIG. 1 DEFINITIONS OF ACCURACY AND UNCERTAINTY
FIG. 2  FLOWCHART OF THE UKAEA WINFRITH CODE ASSESSMENT METHOD
FIG. 3 FLOWCHART OF THE GRS, MUNICH CODE ASSESSMENT METHOD
FIG. 4  FLOWCHART OF THE USNRC’S CODE SCALING, APPLICABILITY AND UNCERTAINTY (CSAU) EVALUATION METHODOLOGY
American Method (CSAU)

Specify scenario (windows), power plant (NPP), code

- Rank processes, phenomena (using e.g. Analytical Hierarchy Process)
- Perform diagnostic analysis (requirements vs code capabilities including scale up, based on documents)
- Establish Assessment Matrix (AM)
- Define NPP nodalisation

- Calculate the SET's & IT's of the AM
- Perform qualitative assessment (consistency, threshold phenomena, scaling distortions?)
- Perform quantitative assessment (determine code accuracy - bias and uncertainty - by comparing test data against code results for key parameters)
- Determine effect of scale and effect of reactor parameters
- Perform NPP sensitivity calculations

- Combine biases and uncertainties
- Produce TOTAL UNCERTAINTIES

MN = Assessment Matrix
NP = Model Parameters
BC = Boundary Condition
NPP = Nuclear Power Plant
IT = Integral Test
SET = Separate Effects Test

European Method (GRS & Winfrith)

Specify transient, window, import. phenomena, models, model parameters

- Calculate SET's (Separate Effects Tests)
- Produce uncertainties of model parameters (NPP)
- Determine uncertainties in boundary conditions (BCs)
- Perform calculations on selected IT's varying NPs and BCs
- Produce uncertainty bands for most important output parameters
- Examiner, if test data are bracketed by the produced uncertainties
- Determine, whether the uncertainties are small enough
- Perform NPP calculations varying NPs and BCs
- Produce uncertainties for important parameters
- Determine, whether the uncertainty bands are narrow enough
- Produce TOTAL UNCERTAINTIES

Fig. 5 A COMPARISON OF CODE ASSESSMENT METHODOLOGIES
MC/QE = Model Correlations / Quality Evaluation
NPP = Nuclear Power Plant

(1) Specify Scenario

(10-1) Specify Test Facility

(10-2) Provide
- Facility Document
- Test Specifications
- Results

(2) Specify NPP

(3) Provide FRPT

(4) Specify Code

(5) Provide
- MC/QE Document
- Code Assessment Reports

(10-3) Distortions in Important Processes

(10-4) Specify Conservative Bias and Distribution

(10-5) Data Base Covers NPP Range

(10-6) Specify Bounding Separate Bias

(10-7) Scale-up Capability in Closure Relations

(10-8) Specify Conservative Bias and Distribution

(10-9) Closure Relations Cover NPP Range

(10-10) Specify Bounding Separate Bias

(12) Perform Sensitivity Calculations

Notes
- Steps (1) to (5) and (12) to (13) are from the CSAU procedure.
- Steps (10-1) to (10-10) are the procedures used to evaluate code scale-up capability.

FIG. 6 GENERIC PROCEDURE TO EVALUATE CODE SCALE-UP CAPABILITY IN THE CSAU METHOD
If the timing of the phenomena in the test and in the calculation is different, a quantitative comparison, based on a mechanical integration of absolute differences, for example, may give results (A better than B) which contradict expert judgement (B better than A).

**FIG. 7** THE INFLUENCE OF TIMING ON QUANTITATIVE DATA COMPARISON

Filtering may be needed when comparing oscillating parameters.

**FIG. 8** THE INFLUENCE OF OSCILLATIONS ON QUANTITATIVE DATA COMPARISON
Dryout is an example of a threshold phenomenon. After dryout the test and calculation may deviate very much, even if the agreement before and at the threshold point was very good.

**FIG. 9** THRESHOLD PHENOMENA AND DATA COMPARISON

It may be meaningless to make code comparisons with data during a particular time period, the reflood period in this example, if there is a large discrepancy already at the beginning of the period.

**FIG. 10** HISTORY EFFECTS AND DATA COMPARISON
USER EFFECTS ON THE THERMAL-HYDRAULIC TRANSIENT SYSTEM CODE CALCULATIONS

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ABSTRACT

Large thermalhydraulic system codes are widely used to perform safety and licensing analyses of nuclear power plants to optimize operational procedures and the plant design itself. Evaluation of the capabilities of these codes are dealt by comparing the code predictions with the measured experimental data obtained from various types of separate effects and integral test facilities. During these comparisons of the code results, there has been a continuous debate on the way how the code user influences the predicted system behaviour. This rather subjective element might become a crucial point with respect to the quantitative evaluation of the code uncertainties which is essential if the “best estimate codes are used for licensing procedures”.

The International Standard Problem Exercises (ISPs) proposed by the OECD-Committee for the Safety of Nuclear Installations (CSNI) and by IAEA (International Atomic Energy Agency) and thermalhydraulic code assessment activity undertaken by US.NRC under International Code Assessment and Application Program (ICAP) demonstrate the large effort put in this framework by organisations all over the world. In recent years, some attempts have been made to establish methodologies to evaluate the accuracy and the uncertainty of the code predictions and consequently judgement on the acceptability of the codes. In none of the methodologies the influence of the code user on the calculated results is directly accounted. In this paper, the results of the investigations on the user effects for the thermalhydraulic transient system codes will be presented and discussed on the basis of some case studies.
The general findings of the investigations show that in addition to user effects, there are other reasons which affect the results of the calculations and are hidden under user effects. These reasons and user effects will be discussed in detail and general recommendations and conclusions will be presented to control and limit them.

1 INTRODUCTION

Within the frame of the large international effort on the assessment of thermal-hydraulic system codes used for the safety analysis of Light Water Reactors, there has been a continuous debate on the way how the code user influences the predicted system behaviour. This rather subjective element might become a crucial point with respect to the quantitative evaluation of the code uncertainties which is essential if the “best estimate codes” are used for licensing procedures.

The International Standard Problem Exercises (ISPs) proposed by the OECD-Committee for the Safety of Nuclear Installations (CSNI) and by IAEA (International Atomic Energy Agency) and thermal-hydraulic code assessment activity undertaken by USNRC under International Code Assessment and Application Program (ICAP) demonstrate the large effort put in this framework by organisations all over the world. In recent years, some attempts have been made to establish methodologies to evaluate the accuracy and the uncertainty of the code predictions and consequently the judgement on the acceptability of the codes. In none of the methodologies the influence of the code user on the calculated results is directly accounted. But the user influence on predicted results became evident in most of the recent ISPs, when different participants using the same code version produced rather different results or, even more severe, predicted completely different system behaviour. The reasons for these differences were mainly attributed to the large choices of different options in order to select appropriate models, correlations or specific multipliers, in case of the first generation codes which are not any longer the state of the art e.g. RELAP3, RELAP4, RETRAN-02, etc. The major reasons of the necessity of options were a) to compensate for the lack of detailed modelling of complex processes (e.g. thermal non-equilibrium effects), b) to allow the use of these codes for both conservative type predictions needed for reactor licensing as well as more realistic best-estimate predictions.

In case of the new generation of advanced computer codes (e.g. TRAC, RELAP5, ATHLET, CATHARE), which are strictly based on two-fluid representation of two-phase flow and “best-estimate” description of complex flow and heat transfer conditions and reflect the state of the art, it was expected that the user effect will be drastically reduced. However, experience with large number of ISPs has shown the dominant influence of the code user on the final results and the goal of reduction of user effects has not been achieved.

In this paper, the results of the investigations on the user effects for the thermalhydraulic transient system codes will be presented and discussed in detail on the basis of some case studies. Finally, general recommendations and conclusions will be presented in order to control and limit some of the user effects, based on the case studies presented.

2 REASONS FOR CODE USER EFFECTS IN RELATION TO INPUT DATA

Some of the main sources by which a code user has influence on the predicted code results are identified and categorized in this Section. Only those effects will be considered which are directly related to code input data. All aspects of changing the code source (code structure, numerical scheme and models), problems related to the code implementation, or the influence resulting from different computer hardware will not be considered here as "code user effect".
2.1 System nodalization

All major existing LWR safety thermal-hydraulics system codes follow the concept of a "free nodalization" which means that the code user has to build up a detailed noding diagram which maps the whole system to be calculated into the frame of a one-dimensional thermal-hydraulic network. To do this, the codes offer a number of basic elements like single-volumes, pipes, branches, junctions, heat structures, etc. This approach provides not only a large flexibility with respect to different reactor designs, but also allows to predict separate effect and integral test facilities which might deviate considerably from the full-size reactor.

As a consequence of this rather "open strategy", a large responsibility is passed to the user of the code in order to develop an adequate nodalization scheme which makes best use of the various modules and the prediction capabilities of the specific code. Due to the existing code limitations and to economic constraints, the development of such a nodalization represents always a compromise between the desired degree of resolution and an acceptable computational effort.

It is not possible here to cover all the aspects of the development of an adequate nodalization diagram, however, two crucial problems will be briefly mentioned which illustrate the basic problem.

Spatial convergency

As has been quite often mis-understood, a continuous refinement of the spatial resolution (e.g. a reduction of the cell sizes) does not automatically improve the accuracy of the prediction. There are two major reasons for this behaviour:

1. The large number of empirical constitutive relations used in the codes have been developed on the basis of a fixed (in general coarse) nodalization.

2. The numerical schemes used in the codes generally include a sufficient amount of artificial viscosity which is needed in order to provide stable numerical results. A reduction of the cell sizes below a certain threshold value might result in severe non-physical instabilities.

From this it can be concluded that there exists no a priori an optimal approach for the nodalization scheme.

Mapping of multi-dimensional effects

Even in relatively small scale integral test facilities, there exist multi-dimensional effects, especially with respect to flow splitting and flow merging processes: e.g. for the connection of the main coolant pipe to the pressure vessel. The problem might become even more complicated due to the presence of additional bypass flows and a large redistribution of flow during the transient. It is left to the code user to determine how to map these flow conditions within the frame of a one-dimensional code, using the existing elements like branch components, multiple junction connections or cross-flow junctions.

These two examples show how the limitations in the physical modelling and the numerical method in the codes have to be compensated by an "engineering judgement" of the code user which at best, is based on results of detailed sensitivity of assessment studies. However, in many cases, due to lack of time or lack of appropriate experimental data, the user is forced to make ad-hoc decisions.
2.2 Code options, physical model parameters

Even though the number of user options has been largely reduced in the advanced codes, there still exist various possibilities how the code user can directly influence the physical description of specific phenomena, e.g.

- choice between engineering type models for choking or use of code implicit calculation of critical two-phase flow conditions,
- flow multipliers for subcooled or saturated choked flow,
- the efficiency of separators,
- two-phase flow characteristics of main coolant pumps,
- pressure loss coefficient for pipes, pipe connections, valves, branches etc.

Since in many cases direct measured data are not available or at least not complete, the user is left to his engineering judgement to specify these parameters.

2.3 Input parameter related to specific system characteristics

The assessment of LWR safety codes is mainly performed on the basis of experimental data coming from scaled integral or separate effect test facilities. Typical for these scaled-down facilities is that specific effects, which might be small or even negligible for the full-size reactor case, can become as important as the major phenomena to be investigated. Examples are the release of structure heat to the coolant, heat losses to the environment, or small bypass flows. Often, the quality of the prediction depends largely on the correct description of these effects which needs a very detailed representation of structural material and a good approximation of the local distribution of the heat losses. However, many times the importance of these effects are largely underestimated and, consequently, wrong conclusions are drawn from results based on incomplete representation of a small-scale test facility.

2.4 Input parameters needed for specific system components

The general thermal-hydraulic system behaviour is described in the codes by the major code modules based on a one-dimensional formulation of the mass, momentum and energy equations for the separated phases. However, for a number of system components, this approach is not adequate and consequently additional, mainly empirical models have to be introduced, e.g. for pumps, valves, separators, etc. In general, these models require a large amount of additional code input data, which are often not known since they are largely scaling dependent. A typical example is the input data needed for the homologous curves which describe the pump behaviour under single- and two-phase flow conditions which in general are known only for a few small-scale pumps. In all these cases, the code user has to extrapolate from existing data obtained for different designs and scaling factors which introduces a further ambiguity to the prediction.

2.5 Specification of initial and boundary conditions

Most of the existing codes do not provide a steady state option. In these cases pseudo-steady state runs have to be performed using more or less artificial control systems in order to drive the code towards the
specified initial conditions. The specification of stable initial and boundary conditions and the setting of related controllers require great care and detailed checking. If this is not done correctly, there exist a large risk that even small imbalances in the initial data will overwrite the following transient, especially for slow transients and small break LOCA calculations.

2.6 Specification of state and transport property data

The calculation of state and transport properties is usually done implicitly by the code. However, in some cases, for example in RELAP5, the code user can define the range of reference points for property tables and, therefore, can influence the accuracy of the prediction. This might be of importance especially in more "difficult" regions, e.g. close to the critical point or at conditions near atmospheric pressure.

Another example is in relation to the fuel materials property data. The specification of fuel rod gap conductance (and thickness) is an important parameter, affecting core dryout and rewet occurrences, that must be selected by the user. Usually this assumption, also connected with the actual fuel burnup, is not reported as a user assumption.

2.7 Selection of parameters determining time step sizes

All the existing codes are using automatic procedures for the selection of time step sizes in order to provide convergence and accuracy of the prediction. Experience shows, however, that these procedures do not always guarantee stable numerical results and, therefore, the user might often force the code to take very small time steps in order to pass through trouble spots. In some cases, if this action is not taken, very large numerical errors can be introduced in the evolution of any transient scenario and are not always checked by the code user.

2.8 Code input errors

In order to prepare a complete input data deck for a large system, the code user has to provide a huge number of parameters (approximately 15 to 20 thousand values) which he has to type one by one. Even if all the codes provided consistency checks, the probability for code input errors is relatively high and can be reduced only by extreme care following clear quality assurance guidelines.
3 SELECTED TYPICAL EXAMPLES FOR IDENTIFYING USER AND OTHER RELATED EFFECTS ON THE CODE CALCULATIONAL RESULTS

In this section, the results of the investigations on the user effects for the thermal-hydraulic transient system codes will be discussed on the basis of some ISP's and other assessment cases.

3.1 ACHILLES Reflooding Test (ISP-25)

One of the transient tests from the best estimate natural reflood series of experiments (Run A1 B105) was selected as ISP-25. The selected transient simulates the end of the accumulator injection in a postulated LOCA with the nitrogen vessel representing the accumulators, which have emptied of water, venting into the cold leg. The detailed information on ISP-25 transient and facility description can be found in [1,2], together with the initial and boundary conditions. A simplified schematic representation of the main active components of the facility is shown in Figure 1.

Participants of ISP-25 were required to provide a pre-test prediction (blind calculation) with no prior knowledge of the experimental results. In addition, all the participants except those participants from the host institution were modelling the facility first time. The pre-test predictions showed a wide variation in results and, even when similar versions of the same code were used, a number of differences were observed with hydraulics and thermal parameters. Comparisons of calculated and experimental data for two such parameters are shown in Figures 2 and 3, see for example [3]. The hydraulic behaviour and heater rod temperature predictions show significant variations. As examples, the range of peak clad temperature predictions is about 150 K and the range of quench times differs by a factor of two.

The comparison of the RELAP5 calculations highlighted the impact of the code user effect, where large variations in predicted behaviour were observed and as a result CSNI sponsored investigations on code user effects based on ISP-25 calculational results [4]. The results of these investigations will be briefly discussed in this section.

The fact that four participants in ISP-25 used the RELAP5 code did provide an opportunity to investigate so-called "user effects", the effect that the code user has on the results of a blind prediction, even though, it is accepted that the code versions were not exactly identical. The outcome of this investigation on user effects led to identification and also differentiation of different sources of discrepancies. The general findings show that in addition to code user effects, there are other reasons which affect the results of the calculations and are hidden under user effects. These reasons are identified as: The effects due to specific characteristics of experimental facilities, i.e. limitations as far as code assessment is concerned; limitations of the used thermal-hydraulic code to simulate certain system behaviour or phenomena; limitations due to interpretation of experimental data by the code users; i.e. interpretation of experimental data base.

Few specific examples will be presented here for each of the identified reasons, as a result of the investigations based on ISP-25 calculational results.

1. The specific characteristics of experimental facilities (i.e. limitations as far as code assessment is concerned):

   1. The analysis of experimental data and the calculations show that the friction loss (K) factors in the ACHILLES test loop were very important. Pressure drop and distribution information were not sufficiently known to the code users in order to obtain the specific friction loss factors properly
around the loop. This point was considered to be the most important one in the analysis of the experiment.

2. The original intention of the test was to provide a constant pressure as a boundary condition. However, the experimenters were not able to maintain a constant containment pressure with the available control system, during the early phase of the transient. This has an influence on the initial pressure distribution in the test loop and has a significant impact on the early behaviour of the experiment.

3. Liquid carry-over was collected at two points: Weir device at the upper plenum and steam separator (Figure 1). The method of steam/water separations was very specific to this test facility. Only the combined flow was measured, as a result it was not possible to determine the ratio of liquid removed at the weir to that at the steam separator. The transient thermal-hydraulic codes including RELAP5 have no capability to simulate this type of device at the top of the core.

4. Formation of ice at the outlet of the Nitrogen tank was not foreseen at the time the ISP-25 was proposed and it is known that such conditions could not be handled with present codes.

5. Other limitations are typical of nearly all intergral loop experiments e.g. quench front progression measurements (assumed to be one dimensional), minor errors in the flow rate measurements with turbimeter due to the introduction of the nitrogen into the system (mainly calibration problem).

II. Limitations of the used thermal-hydraulic codes to simulate certain system behaviour or phenomena:

It should be clearly stated that all the thermal-hydraulic codes have their own, specific limitations. Since the versions of the RELAP5 code were used for these investigations, the limitations will be restricted to this code and its applications to ISP-25. Main limitations coming from code modelling are:

1. RELAP5 was not able to deal with non-condensable gases in the system. The numerics in relation to the treatment of non-condensibles appeared to be very poor.

2. RELAP5 calculates the pressure drop on the basis of the defined geometry as input to the code calculation, however, the calculated pressure drop can be quite different, if the loss coefficients and the details of the geometry are not properly supplied.

3. Inverted annular film boiling (IAFB) would probably have occurred during the initial surge due to subcooled liquid reaching the quench front. RELAP5 and also the other codes do not model the main aspects of the fast forced and low pressure refood conditions as encountered during the initial phases of the transient in ISP-25.

4. There were no specific models in RELAP5 to simulate the effects of the grids.

5. Ice formation, at the outlet piping of the accumulator, due to the nitrogen injection is not modelled in RELAP5 or in other system codes.

6. RELAP5 has no capability for calculating the liquid film formation and flow on wetted surfaces, e.g. at the top of the core and on the shroud vessel.

7. The modelling and calculations of the entrainment and de-entrainment for various geometrical configurations are very limited with RELAP5 and also with the other system codes.

III. Limitations due to interpretation of experimental data by the code user:

The predicted scenario of each of the participants was strongly dependent on typical user choices, e.g. consideration of pressure drop distribution, simulation of the nitrogen vessel, etc. The results of the four RELAP5 users are very different, mostly, due to the different philosophies in performing the calculations in order to overcome some of the code deficiencies present at the time of the calculations. Some of the specific examples are: One of the code user, due to the deficiencies of the IBM version of the code, was not able to use the "reflooding option" and instead the "blowdown option" with corresponding heat transfer package of the code was used. Some of the code users simulated nitrogen behaviour in the accumulator with "saturated steam" or "hydrogen".  

In addition to the user choices, interpretation of the boundary conditions by the code users has an important effect on the results. Some typical examples are:
1. Functioning of the capacity vessel was misinterpreted by the code users. The pressure of the capacity vessel as boundary conditions was determined in the experiment by steam and nitrogen coming into the loop. This quantity was assumed as an independent boundary condition in the code calculations. It was believed that this point has an effect only during the first 30 seconds of the transient.

2. Interpretation and use of the friction loss (K) factors which were supplied in the ISP-25 report on Boundary Conditions and Experimental procedure for ACHILLES Run [2] were done differently by different code users. Wide variations of user choices were available as far as the use of the K factors at two different orifices.

3. Differences in relation to the initial conditions in various input decks depended on the fact that the experimental initial conditions were not at true steady state. Therefore different procedures were adopted to establish these conditions by various users.

4. In the experiment, most of the water passed into the steam separator and the steam passing through the orifice, was saturated. This situation was not considered by the code users.

3.2 LOBI Natural Circulation Test

The post-test analysis of LOBI A2-77A natural circulation test has been independently performed by six different users (CENG-Grenoble, CEA-Paris, GRS-Garching, JRC-Ispra, DCMN-Pisa and UKAEA-Winfrith), using five different advanced thermal-hydraulic codes (CATHARE 1 V1.3, TRAC-PFI/MOD1, ATHLET/MOD1.0, RELAPS/MOD1-EUR and RELAPS/MOD2 by the last two organizations) [5 and 6]. The detailed information on the natural circulation test A2-77A can be found in [7]. On the basis of these post-test analysis results, interaction of code users with the mentioned system codes have been analyzed. The first point to be considered is the comparison of the input decks of different codes. At this stage, it can be identified that the most significant limitation of a current code analysis is the lack of detailed insights on the used input parameters. All these major existing thermal-hydraulic safety codes follow the concept of a "free nodalization". This means that the code user has to build-up a detailed nodal diagram which simulates the whole system to be calculated into a frame of a thermal-hydraulic network.

As a consequence of this rather wide open strategy, considerable responsibility is passed to the user of the code in order to develop an adequate nodalization scheme which makes best use of the various modules and the prediction capabilities of the given code. The final product is a "model" that represents a compromise between the user knowledge of the code performance, of the facility hardware and of the simulated transient scenario. In addition, the existing code capabilities and limitations and the required computer CPU time also play a considerable role in defining the specifications of the input model.

On the basis of the above discussion, and in order to establish a critical comparison of the nodalizations and specific assumptions, twenty different items related to the input decks are compared among each other, as given in table 1. These have been split into three main groups:

1. The elements characterizing the nodalization details (items 1 to 5 in table I):
   
   In relation to the number of nodes, on one hand RELAPS and ATHLET codes, with roughly 150 nodes, on the other hand the CATHARE code, with more than 300 nodes, and the TRAC code, with 250 nodes, can be distinguished (Figure 4).
   
   This choice is only partially due to the user; instead the code numerical structure plays an important role for establishing the degree of detail of the nodalization model. As an example, RELAPS code, owing to the Courant limit, needs nodes having length larger than few tens of centimeters, while CATHARE code does not have such a constraint, allowing a greater degree of freedom.

   In principle, the best results for a physical simulation should be given by a nodalization with a number of nodes as large as possible, however, this is not strictly true for the current system
codes. Otherwise, an optimal number of nodes can be recognized for each code for a given simulation problem. Directions for the selection of this number are not available in any code manual. Only the user experience can achieve this parameter, considering the phenomena to be analyzed, in line with the available resources (CPU time needed, computers, etc.) and the goals of the study (e.g., sensitivity analyses aiming at the interpretation of physical phenomena, licensing calculations, etc.). It should be noted that a large amount of sensitivity analyses can bring substantial improvements of nodalization parameters; in this way coarse nodalization, with few elements, can produce better results than fine nodalization with much larger number of elements.

Concerning the overall number of heat structure mesh points (item 5 in table I), the dominant influence of this parameter on the heat transfer mechanism must be stressed. In particular, the heat release from structures is strongly affected by the number of meshes (Figure 5).

2. The elements characterizing the "nodalization fidelity" to the actual geometrical data of the facility (items 6 to 11 in table I):

The geometrical fidelity of the nodalization and the system hardware is a specific characteristic of a code input model that should be considered very carefully. Apart from the nodalization detail, which is essentially a user choice, the agreement between input and actual system related parameters is an objective goal to be pursued. With reference to the overall volume of a facility that can be considered the most important parameter in this group, the following approximations or inadequacies affect the agreement between actual system value and code input value:

- imperfect knowledge of the system or the plant data (drawings inadequacies, etc.);
- presence of dead ends (nozzles, instrumentation lines, etc.);
- need to simulate a three dimensional configuration with a one dimensional code modules.

For these points the compromises needed to be done are based on the user knowledge and experiences. The experience demonstrates, for example, the acceptability of few percent error on the nominal value of the overall facility volume.

Another aspect of the nodalization fidelity is related to the active heat transfer areas, i.e., heat sources and sinks. In relation to this, the most important characteristics to be preserved are the total core area and the overall steam generator heat transfer surface. Usually, these parameters are respected with an error less than 1%.

Two further problems, typically encountered by a system code user, arise during the development of a nodalization:

a) With reference to a PWR typical plant, the choice of the hydraulic channel numbers in the steam generator and in the core (for a BWR plant the same problem may occur in relation to the number of jet pumps and again to the number of core channel [8]). A nodalization with only few "pipes" can preserve the overall thermal energy balance, however cannot represent a nonuniform flow behaviour of the various channels (e.g., nonuniform flow distribution in the steam generator U-tubes, or in the lower plenum of reactor vessel or in the steam generator plena, channel to channel oscillations, etc. [8]);

b) Passive structures of the plant. The consideration of all the material structures including vessel, piping and internal wall, as well as flanges, valves and pump casings is almost impossible owing to limitations of computer memory. Approximations are needed and are usually done.

3. The elements characterizing the relation between actual hydraulics and geometry of the code input model (items 12 to 20 in table I and Figures 6, 7 and 8):

This topic refers, essentially, to local and distributed pressure drop coefficients. With reference to the friction factor, a substantial difference among the various basic models implemented in the codes can be observed, e.g., the dependency of friction factor upon the Reynolds number is not considered in the same way in all codes: for example Colebrook correlation is used in RELAP5/Mod2 and some other special correlations are adopted by TRAC and CATHARE.
Roughness is only accounted for by the RELAP input deck, while it has no influence on calculated pressure drops in CATHARE and TRAC.

Finally, with regard to the form loss coefficients, items 12 to 18 in Table 1, the following remarks can be made:

a) there is no theoretical model suitable to calculate this parameter in the wide variety of configurations encountered in modelling a typical nuclear plant or simulator; still no relationship gives the dependency of these factors upon Reynolds number and local void fraction;

b) experimental uncertainties are often connected to this parameter, usually derived from pressure drop measurements;

c) loss coefficient values have to account for the three-dimensional effects that can not be modelled by a one-dimensional code;

d) some of the thermal-hydraulic model deficiencies can be adjusted by use of loss coefficients in an artificial way.

The data reported in Table 1 give an idea of typical variation ranges associated with assumed values of these parameters. It should be noted that "all" the above values lead to "reasonable agreement" with experimental data at least as far as the initial steady state is concerned (Figure 9).

Each code user, depending on the code used, interprets in a subjective way the uncertainties characterizing the experimental data. The rest of the steady state and transient calculations proceed on the basis of these interpretations and assumptions. In order to give an idea of the consequences of the choices discussed above, the calculated flow rates in intact loops during the whole transient are compared with the measured values in Figure 10 as a function of the overall residual mass of the primary side. In general, the codes used capture the basic physics of the involved phenomena, but the quantitative values of the physical variables are not satisfactory. More in-depth analyses [9] show that, apart from possible inadequacies of input decks discussed above, there are also some limitations of the thermal-hydraulic models, e.g., flooding, stratification in horizontal pipes, etc.

3.3 Additional ISP Calculational Cases

Some of the user effects which have been identified based on the ISP exercises performed recently (ISP-22, ISP-26 and ISP-27) will be summarized and presented, as further examples, in this section.

ISP-22

This ISP was a "double blind" exercise based on a special transient in the SPES facility [10]. The examined test, named SP-FW-02, was initiated by a loss of feedwater in the secondary side of the steam generator in loop one.

A very broad range of results has been obtained by participants using the same code (Relap5/Mod2) (e.g., Figure 11). Extensive post-test analysis and discussions at a specific workshop [11] demonstrated that two of the main sources of discrepancies were:

(a) approximate consideration of heat losses from primary side with main reference to their distribution: in particular small changes in the pressurizer related value (within experimental uncertainties) shift the sequence of relevant events of thousands seconds, leading, possibly, to different conclusions about the validity of the accident management procedure selected in the experiment;

(b) wrong calculation of initial mass of steam generators secondary side, notwithstanding a known code limitation in calculating the total mass inside a boiler (such as the secondary side of a steam generator). For the post-test analysis, this limitation could be accounted for through proper adjustments of user selected parameters in the nodalization.
Item (a) is a demonstration of the criticality of the interface between the code user and the definition of boundary condition: the code user should fully understand the meaning and the limitations of the boundary conditions available from the experiment. Item (b) demonstrates the need for the code user to know in some detail the basic code limitations, and possibly, to adopt tools suitable to overcome these deficiencies.

The consideration, for one of the participants of both the mentioned aspects led to the comparison between measured and calculated trends shown in Figures 12 and 13.

ISP-26

ISP-26 was an open standard problem based on a 5% cold-leg small break LOCA experiment conducted in the ROSA-JV Large Scale Test Facility (LSTF) [12]. Wide dispersion bands were obtained between the calculated results of the different participants and the experimental data, especially in relation to the prediction of the heater rod surface temperature trends (Figure 14).

The evaluation of data base constituted by experimental and calculated trends led to the identification of four areas connected in some way with the user, that are among those responsible for discrepancies:

1. The modeling of core and steam generator upflow side as well as the different choices for the break flow calculation contributed to the results of the calculations significance [13].
2. The convergence of the solutions with respect to optimization of the nodding was not assured. A "well balanced" nodalization with a relatively fine nodding in steam generators and core (as far as practical) may produce better results than an unbalanced nodalization where, as an example, a large number of nodes are used for the core but a coarse noding is used for steam generators.
3. The calculated results are much affected not only by physical options but also by numerical options, i.e., convergence criteria and numerical scheme. These options are selected by each user based on their own criterion. Thus, it would be desirable that the appropriate guidelines and information are provided to users for selecting input options [13].
4. The dead end volumes and the fluid temperatures inside dead ends may affect the overall energy and mass balance during the transient. These were not completely specified by experimentalists.

ISP-27

The ISP-27 was classified as a "double blind" international standard problem. It was based on the BETHSY small-break LOCA test 9.1.b. It consists of a 2" cold-leg break experiment in which high pressure safety injection (HPSI) system is assumed unavailable. It belongs to the multiple failure transient category and is addressed to accident management studies. The resulting transient leads to a large core uncoverage and fuel heat-up requiring the implementation of an ultimate procedure [14].

Preliminary observations from the comparison Workshop on ISP-27 indicate that the differences between blind calculations and experimental data were mainly coming from the user effects. Most important of these were:

1. Achieving true steady state and overall system balance was specially important for this long transient. This point was overlooked by most of the code users.
2. Nodalization of the BETHSY system should have been established considering the anticipated important phenomena during the transient. User experience has very much influence on this aspect.
3. Considering the specific features of the break nozzle, the experimenters provided the participants with experimental data from a separate effect test facility, using a nozzle similar to those installed in the integral facility.
A correct use of this data base should have produced critical flowrate values very similar for the various participants as a function of pressure. Actually this was not the case due to various reasons (Figure 15), some of these are:
- errors in interpreting the supplied data base;
- judgement of low importance of this data for the overall transient evolution in an integral test facility;
- influence of conditions in the upstream pipe upon the calculated critical flowrate that actually made useless the comparison of data calculated in the integral facility with those measured in the separate effect test facility.

4 SOME SUGGESTED WAYS TO REDUCE THE USER EFFECTS

From what has been mentioned above it is quite clear that, at least for the presently existing codes and their limitations with respect to physical modelling and numerical techniques, there is no chance to completely avoid the influence of the code user on the predicted system behaviour. However, some ways how the magnitude of the user effect might be reduced are indicated in the following.

4.1 User Training

As has been described above, a large responsibility is left to the code user in order to elaborate an adequate description of the facility and to prepare the corresponding input data. This task can be fulfilled only if the user is fully aware of the physical modelling and the limitations of the codes, if he has a sufficient knowledge of the facility to be described and if he also has a good understanding of the major phenomena expected to occur during the transient. Therefore, user instruction and training might be the easiest way in order to improve the quality of the code predictions.

Unfortunately, there has been in the past a tendency to use ISPs as a type of "fast course" in training new code users without giving importance to above mentioned aspects. The rather poor results produced in these cases have largely contributed to the confusion on the user effect and the evaluation of the code capabilities.

4.2 Improved User Guidelines

There has been in the past a continuous demand for user guidances which should be based on the results of a systematic code assessment programme. More detailed user guidelines are certainly a way to improve the quality of code prediction and to avoid larger mistakes and, in this sense may also reduce the user effect. However, to be more realistic, such guidelines cannot give detailed recipes for all existing conditions and, therefore, cannot substitute for a trained and experienced code user.

4.3 User Discipline

Even the best user guideline will not serve useful purpose if the code user, as often found is keen to invent "tricks" in order to drive the code prediction towards an experimental result or towards what the user expected to achieve. This does not contest the value of sensitivity studies which are often the only way to better understand code deficiencies. What is meant
here is a type of "tuning" of the results by a selection of completely unrealistic input values for physical models related parameters or boundary conditions, or by the extensive use of parallel channel and cross flow junctions in order to produce some "multi-dimensional" flow calculation which is beyond the code's capability. The result of this "tuning" is at best a compensation of errors which only contributes to confusion on the true prediction capability and, therefore, on the clear identification of code deficiencies and limitations.

4.4 Quality Assurance

The preparation and testing of an input deck for a reactor or a related integral test facility is a tedious work which requires even for a competent code user an effort of about one man-year. A reliable input deck can only be achieved if a clear quality assurance strategy is followed. Often this effort is not allocated (e.g. due to lack of time, money, or competence) and, consequently, incomplete or error-ridden decks are used. A bad habit has been also that, in order to save time, existing decks are shared between different users who then introduce only minor modifications without a complete checking of the major part of the input data and without an understanding for what purpose they have been developed.

4.5 Code Improvement

For a long time perspective, the best way to reduce the user effect would be to improve the code with respect to the physical modelling and the numerical techniques. This might be illustrated in two examples:

- The use of numerical methods which allow an automatic mesh refinement based on actual local flow conditions (e.g. for the representation of tracking mixture levels or quench fronts). This would avoid the code user having to make a prejudgement on the minimum cell or mesh size for a specific system component.

- To include multi-dimensional capabilities for those specific parts of the system where these effects are dominating, even in small-scale test facilities: e.g. for the downcomer (see for example the 2-D downcomer module in CATHARE-2), or for the upper and lower plena in the pressure vessel. This would take from the user the need to make unqualified judgements on the major flow conditions to be expected during the transients.

A possibly less ambitious improvement to the present codes could be the implementation of an improved user interface which make use of modern graphics, window techniques and drop-down menus. This would not only reduce the tedious workload to elaborate code input data sets, it would also reduce the probability for code input errors and, therefore, would contribute to minimize the code user effect.
5 CONCLUSIONS

Experience with some code assessment case studies and also additional ISPs have shown the dominant effect of the code user on the predicted system behaviour. The general findings of the user effect investigations on some of the case studies indicate, specifically, that in addition to user effects, there are other reasons which affect the results of the calculations and are hidden under the general title of "user effects". These additional reasons are identified as: The specific characteristics of experimental facilities, i.e. limitations as far as code assessment is concerned; limitations of the used thermal-hydraulic codes to simulate certain system behaviour or phenomena; limitations due to interpretation of experimental data by the code user, i.e. interpretation of experimental data base.

On the basis of the discussions in this paper, the following conclusions and recommendations can be made:

- More dialogue appears to be necessary with the experimenters in the planning of code assessment calculations, e.g. ISPs.

- User guidelines are not complete for the codes and the lack of sufficient and detailed user guidelines are observed with some of the case studies.

- More extensive user instruction and training, improved user guidelines, or quality assurance procedures may partially reduce some of the subjective user influence on the calculated results.

- The discrepancies between experimental data and code predictions are due both to the intrinsic code limit and to the so called "user effects". There is a worthy need to quantify the percentage of disagreement due to the poor utilization of the code and due to the code itself. This need especially arises for the uncertainty evaluation studies (e.g. [15]) which do not take into account the mentioned user effects.

- A very focused investigation, based on the results of comparison calculations e.g. ISPs, analysing the experimental data and the results of the specific code in order to evaluate the user effects and the related experimental aspects should be integral part of the ISP process. This may also help to quantify the "user effects".
References


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<th>ITEM</th>
<th>TRAC/PF1</th>
<th>CAT1 V1.3</th>
<th>RS/M2</th>
<th>ATHLET</th>
<th>R5/M1-EUR</th>
<th>R5/M2</th>
<th>EXP</th>
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<td>0.522</td>
<td>0.524</td>
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<td>7. free total volume of IL SG SS (m³)</td>
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<td>10. fluid mass in IL SG SS (kg)</td>
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<td>11. fluid mass in BL SG SS (kg)</td>
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<td>35/35</td>
<td>27/27</td>
<td>20/2</td>
<td>2.3/2.3</td>
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<td>13. K_f/K_w at SG IL inlet of the U-tubes</td>
<td>9/9</td>
<td>0.26/0.26</td>
<td>20/20</td>
<td>12/12</td>
<td>0.36/0.36</td>
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<td>14. K_f/K_w at SG BL inlet of the U-tubes</td>
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<td>2.05/0.35</td>
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PRZ=Pressurizer, IL=Intact Loop, BL=Broken Loop, HL=Hot Leg, CL=Cold Leg, SG=Steam Generator, SS=Secondary Side, PS=Primary System, PU=Pump, UH=Upper Head, RPV=Reactor Pressure Vessel, K_f=Forward Loss Coefficient, K_r=Reverse Loss Coefficient

Table I: Items related to the LOBI-MOD2 natural circulation test input decks
Figure 1: ACHILLES rig configured for best-estimate transients

Figure 2: ISP-25, combined downcomer and core collapsed liquid levels. Comparison of experimental and calculated data
Figure 3: ISF-25, cladding temperature history at 2.01 m elevation. Comparison of experimental and calculated data.

Figure 4: Number of nodes chosen by various users for nodalizing the LOBI facility.

Figure 5: Overall number of structure mesh points chosen by various users for nodalizing the LOBI facility.
Figure 6: Forward pressure drop coefficient at intact loop connection with pressure vessel of LOBI facility as selected by various users.

Figure 7: Pressure drop over steam generator intact loop primary side resulting from steady-state calculations for LOBI facility by various users and comparison with experimental data.

Figure 8: Pressure drop over steam generator broken loop primary side resulting from steady-state calculations for LOBI facility by various users and comparison with experimental data.

Figure 9: Distribution of fluid temperature along intact loop resulting from steady-state calculation by various users and comparison with experimental data.
Figure 10: Comparison between measured and calculated trends of steady-state mass flow rate in intact loop during the whole transient as a function of primary side mass inventory.

Figure 11: Dispersion of results obtained by participants performing "blind" calculations for ISP-22 (SPES facility) by using RELAP5/Mod2 code.

Figure 12: Post-test calculation of a participant for SPES SP-FW-02: Upper plenum pressure.

Figure 13: Post-test calculation of a participant for SPES SP-FW-02: Pressurizer level.
Figure 14: Heater rod surface temperature (maximum): Calculated dispersion bands for RELAP5/Mod2 participants for ISP-26, ROSA-IV-LSTF, open standard problem.

Figure 15: Integrated break mass flow comparisons between experimental data and RELAP5/Mod2 calculations (Blind), for BETHSY (ISP-27).
SCALING AND COUNTERPART TESTS

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Extended Abstract

Nuclear Reactors are very large and complex systems. The capability to define realistic plant scenarios in case of off-normal transient conditions, largely depends upon measurements taken in relatively small scale rigs or upon the reliability of system codes which, in turn, can be assessed only against data taken in the same experiments.

The extrapolation of a small thermalhydraulic loop behaviour or of the accuracy of a thermalhydraulic code to the prototype conditions, constitutes a fundamental problem in this area. On one hand unavoidable distortions characterize the design and the operation of small facilities, on the other hand numerical models and code use strategy, even if qualified from the comparison with the experimental data, cannot be retained reliable in plant calculations too.

The objective of this paper is an attempt:
- to scale-up phenomena measured in small scale facilities;
- to scale-up code accuracy (defined independently).

A preliminary evaluation of various possibilities led to the conclusion that volume scaling is the most suitable way to extrapolate experimental scenarios and code accuracy.

The results obtained in relation to four different test scenarios are discussed:
1. BWR small break LOCA counterpart tests (experiments performed in Piperone, First and Rosa-III facilities; calculations in the same facilities and in the Caorso BWR plant);
2. PWR natural circulation similar tests (experiments performed in Semiscale, Spes, Lobi, Pkl, Bethsy, and Lstf facilities; calculations in Spes, Lobi, Lstf and Doel PWR plant);
3. PWR small break LOCA counterpart tests (experiments performed in Lobi, Spes, Bethsy and Lstf);
4. PWR loss of feedwater similar tests (experiments performed in Lobi and Spes; calculations in the same facilities and in Krako PWR plant).

In this context similar tests are experiments where similar phenomena occur in differently scaled facilities: test scenarios and sequence of events are qualitatively the same. Counterpart tests are similar tests where boundary and initial conditions have been defined, as far as possible, on the basis of suitable scaling criteria.

'Dispersion bands' have been obtained from the BWR small break LOCA
activity including the experimental curves and the plant prediction
with a nodalization having the feedback from the post-test analysis of the
mentioned experiments. 'Average accuracy' has been defined and
extrapolated; in the case of natural circulation in PWR, it has been
compared with code uncertainty obtained from sensitivity analyses.

In the case of PWR small break LOCA it was shown that unavoidable
differences in hardware and in boundary and initial conditions make
unsuccesfull the attempt to scale-up to plant situations single measured
parameters. Furthermore, in the case of the loss of feedwater experiments,
large influence was found of the above conditions upon the overall test
scenarios: in the Spes facility, delayed activation of the emergency
feedwater was sufficient to recover the accident; this was not the case in
Lobi. It was difficult to judge (or to scale-up) the effectiveness of this
accident management procedure in real plant conditions owing to the code
unability to predict the measured phenomena.

Nevertheless, the need to use system codes for scaling analyses is
confirmed from the overall activity.
SESSION 4

APPLICATION OF COMPUTER CODES

Session Chairman:

G. Santarossa, ENEA, Bologna
SESSION 4

SUMMARY

G. Santarossa

The fourth session specifically addressed the status of code application to real plant simulation covering three main subjects:

1 - Application of codes to accident sequences beyond design, with particular reference to the TMI-2 accident.
2 - Code assessment and model qualification with real plant data.
3 - Application to the study of A.M. procedures.

The analysis of the TMI-2 accident was performed within CSNI by an ad hoc group, in collaboration with the U.S. Department of Energy. It was a unique opportunity to benchmark severe accident analysis methods against full scale, integrated facility data. Some of the conclusions from the CSNI report on the TMI-2 analysis exercise were recalled to point out the progress made since then, as an overview in the first presentation.

The participants' analyses reasonably simulated the accident until the start of core heat-up, but were unable to simulate system response to the 2B reactor coolant pump transient. Between the start of core heat-up and the pump transient the analysis diverged. Some codes (e.g. SCDAP/RELAP5) predicted pressurizer drainage for base case boundary conditions. This represents a potentially non-conservative analysis. Sensitivity analysis performed with RELAP showed that the presence of hydrogen, eliminating condensation, controls pressurizer drainage. This shows that the calculations are qualitatively correct (if not quantitatively correct), that sensitivity studies are essential in identifying the phenomena interactions that determined the progression of the TMI-2 accident.

From the presentation, and the discussion which followed, it was not possible to conclude that any progress had been made in the simulation capability of accident progression after the start of core damage: a phase which implies geometric deformations, development of hydrogen and non-condensable gases, phenomena for which models have been developed which require experimental confirmation, including validation on separate effect tests, and which justify on-going experimental programmes.

The process of model qualification and code assessment against real plant data was the subject of a survey in the second presentation. The problems associated with the process of building a plant model were discussed, highlighting the importance of the data acquisition, the qualification process and the detailed knowledge needed of control and system performance, which, following plant development, has to be verified from design specifications to the results of the as-built qualification tests.

The importance of simulating Balance Of Plant systems (BOP) was underlined. This is one of the reasons suggesting that a plant model structure has to be developed by sections or by modules which have to be coupled and to be used in connection with the specific transients to be studied.
Some of the main conclusions:
- Comparisons with real plant transients have advantages for code assessment but also severe limitations.
- When coupled with verified plant models, the existing codes have already demonstrated their capability to help understanding the interaction of plant systems and thermalhydraulic phenomena under real transient conditions.
- Detailed analysis of global transient measurements and decoupling of the thermalhydraulic sections are required to obtain conclusions.
- The generalized use of data acquisition systems on the plant is very beneficial.

International exercises on plant transients, scaling groups classification and qualification matrices of plant models were recommended together with an effort to encourage and promote the availability of plant data from vendors and utilities.

The last presentation underlined the developments and assessment of the computer codes under-way in Germany (ATHLET), in the perspective of use for accident management analysis. The objectives are the use of BE models to explore accident sequences beyond design, to investigate operating instructions and procedures to prevent core damage and mitigate consequences.

Calculation of accident sequences and assessment on tests simulating these sequences showed that model improvements are needed, for example phase separation, condensation, break flow evaluation. Development directions were also identified in the areas of input diagnostics - steady state evaluation - BOP modelling and numeric solutions in order to shorten computing time.
TMU-2 Analysis

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The accident at Three Mile Island Unit 2 (TMU-2) provides an opportunity to benchmark severe accident analysis methods against full-scale, integrated facility data. In collaboration with the U.S. Department of Energy (DOE), the OECD Nuclear Energy Agency established a joint task group to analyze various periods of the accident and benchmark the relevant severe accident codes. In this paper the author presents one result from the TMU-2 analysis exercise that may be of interest in evaluating thermal hydraulics codes.

Arguably one of the more interesting aspects of the analysis results is the predicted pressurizer level response during core heatup. A brief review of the accident scenario is relevant to understanding the pressurizer behavior and the calculations. Feedwater and turbine trips initiated the accident upon loss of condensate suction. When the pilot operated relief valve (PORV) opened in response to the reactor coolant system (RCS) pressure increase caused by the loss of heat sink it stuck open. The operators did not notice the stuck open PORV, but noticed the full pressurizer. The operators terminated high pressure injection and initiated shutdown flow. The net loss of coolant through the PORV and shutdown system caused the RCS coolant inventory to decrease to the point at which the RCS pumps could not be operated. By ~100 min the operators had tripped all four RCS coolant pumps, and core heat-up started at ~110 min. At ~140 min the operators closed the PORV block valve terminating the loss of coolant through the PORV. However, flow through the shutdown system continued. The operators restarted the 2B RCS pump at 174 min for ~1 min. The restart pumped about 30 m³ of liquid into the reactor vessel. RCS pressure increased rapidly during the 2B pump transient.

The above scenario is summarized in Figure 1 showing the measured primary and secondary system pressures. At ~30 min into the accident RCS pressure had decreased to secondary saturation pressure, and continued to follow the A-loop secondary pressure until about 125 min. At ~125 min RCS pressure started to increase departing from the A-loop once through steam generator (OTSG) secondary side pressure. The author infers that energy was no longer being transported to the A loop OTSG after about 125 min. Hydrogen generation due to cladding oxidation probably caused the loss of energy transport to the A-loop OTSG. RCS pressure continued to increase until the end of the 2B pump transient.

The measured pressurizer level and RCS pressure for 100 to 200 min after turbine trip are shown in Figure 2. After the last RCS pump trip the pressurizer level decreased following the decreasing RCS pressure. When the operators closed the PORV block valve the pressurizer level stabilized at a constant level. In the TMU-2 plant
a U-tube surge line as shown in Figure 3 connects the pressurizer to the hot leg. The U-tube arrangement could allow the pressurizer and surge line to act as a manometer responding to the pressure differential between the hot leg and pressurizer dome. If hot leg pressure is above the pressurizer dome pressure, then some liquid will be retained in the pressurizer. Increasing RCS pressure at PORV block valve closure trapped the coolant inventory in the pressurizer.

The TMI-2 analysis exercise participants found that under some conditions the severe accident analytical methods predicted that a severe accident might not have occurred due to pressurizer drainage after PORV block valve closure. Predicted drainage of the pressurizer cools the core delaying core heat-up. Codes such as SCDAP/RELAP5 predicted drainage depending on make-up flow rate, see Figure 4. In effect a bifurcation in pressurizer response is predicted. If liquid is converted to vapor (steam and/or hydrogen) in the core at a rate that is greater than the condensation rate, then RCS pressure increases, and the pressurizer inventory remains constant or increases. In this paper the author presents a sensitivity analysis of the factors that may influence the calculated pressurizer level after PORV block valve closure.

The phenomena considered include hydrogen generation rates, primary to secondary heat transfer, and energy generation in the core. The sensitivity analysis was conducted using RELAP5, thus allowing improved control of the parameters of interest. Figure 5 shows the SCDAP/RELAP5 compared to the RELAP5 calculations for a make-up flow rate (MUF) of 0 kg/s. At a MUF = 0 kg/s RELAP5 predicts drainage of the pressurizer, while SCDAP/RELAP5 predicts inventory hold-up in the pressurizer. The RELAP5 calculation for MUF = 0 kg/s is the base case for these sensitivity studies.

Parameter variations were generally initiated at 120 min. For the cases with hydrogen injection the injection rate was assumed to be a constant at either 0.01, 0.05, 0.1, or 0.5 kg/s. An injection rate of 0.1 kg/s is about equal to the average hydrogen generation rate for the SCDAP/RELAP5 base case. Another set of variations removed primary to secondary heat transfer at 120 min. I considered three cases: (a) nominal estimation of heat transfer before OTSG removal, (b) ten percent reduction in primary to secondary heat transfer before OTSG removal, and (c) 1/3 reduction in primary to secondary heat transfer before OTSG removal.

The effect of hydrogen injection is summarized in Figure 6. For the hydrogen injection rates of 0.01 and 0.05 kg/s do not lead to retention of the pressurizer liquid inventory. Pressurizer liquid inventory is retained for an injection rate of 0.5 kg/s (∼5 times the SCDAP/RELAP5 calculated hydrogen generation rate). At an injection rate of 0.1 kg/s it is not clear that inventory will or will not be retained since RELAP5 aborted.
Another comparison is to the case where 25% additional decay heat, and hydrogen to simulate cladding oxidation heat. The calculated pressurizer level is shown in Figure 7 for this case, 0.1 kg/s hydrogen flow, and the SCDAP/RELAP5 base case. The calculated pressurizer level is about the same in all three cases. It may be concluded that hydrogen generation, and oxidation energy plays a direct roll in pressurizer level response.

The author also considered the primary to secondary heat transfer rate as one of the parametric sets. All primary to secondary heat transfer through the steam generator tubes was removed from the base case calculation at 120 min. For the parametric cases the heat transfer rate before OTSG heat transfer removal was reduced by 10% and 33%. These calculations are summarized in Figure 8. All three calculations indicate pressurizer drainage before 174 min. However, decreasing primary to secondary heat transfer does increase the time to the onset of drainage.

The calculated pressurizer response is a result of the pressure difference between the hot leg and pressurizer dome. In figures 9 and 10 the differential pressure (hot leg minus pressurizer) and pressurizer level for 0.1 kg/s hydrogen injection and OTSGs removed are compared. The author found that the pressurizer drained when the pressure differential fell below about 0.04 MPa (~6 psid). This occurred in all cases where RCS pressure did not increase or could not be sustained to ~174 min. For example, Figure 11 shows the calculated RCS pressure for a number of cases. Only in the cases with sustained increasing RCS pressure after 140 min are associated with pressurizer inventory retention.

A number of conclusions may be drawn from the predictions of pressurizer level. First the predicted pressurizer level after FORV block valve closure is determined by thermal hydraulics as well as the calculation of hydrogen generation. Second it is possible to arrive at a nonconservative prediction of pressurizer level (pressurizer drainage). Although some aspects of severe accidents may not depend greatly on thermal hydraulics, it is the author's conclusion that predictions of severe accidents require severe accident models properly coupled to reliable thermal hydraulics models.

REFERENCES

1 This work was supported by the U.S. Department of Energy (DOE), Assistant Secretary for Nuclear Energy, Office of Light Water Reactor Safety and Technology, under DOE contract DE-AC07-76ID01570.
3 TMI-2 Joint Task Group, TMI-2 Analysis Exercise, to be published.
Figure 1. TMI-2 measured system pressures.

Figure 2. TMI-2 measured pressurizer level.
Figure 3. Pressurizer arrangement.

- Measured
- Base case (HPI = 6.5 kg/s 0 < t < 100 min
  = 4.0 kg/s 100 < t < 200 min)
- Paremetric (HPI = 6.5 kg/s 0 < t < 100 min
  = 2.0 kg/s 100 < t < 200 min)

Figure 4. SCDAP/RELAP5 calculated pressurizer level vs. measured level.
Figure 5. Comparison of SCDAP/RELAP5 and RELAP5 calculations of pressurizer level.

Figure 6. Calculated pressurizer level response to hydrogen injection.
Figure 7. Calculated pressurizer response to simulated oxidation energy.

Figure 8. Calculated pressurizer level for steam generator heat transfer variations.
Figure 9. Hot leg-Pressurizer differential pressure vs. pressurizer level - 0.1 kg/s hydrogen injection.

Figure 10. Hot leg-Pressurizer differential pressure vs. pressurizer level - no steam generators, 67% heat transfer 100 min < time < 125 min.
Figure 11. Calculated RCS pressure.
EXPERIENCE FROM THERMALHYDRAULIC PLANT CALCULATIONS
MERITS AND LIMITS

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ABSTRACT

The paper describes the problems associated with building
a Nuclear Plant Model for simulation of transients. The
structure of the models and the interplay between different
sections is reviewed, followed by a discussion of the use of
plant data for assessment of the one dimensional
thermalhydraulic codes. Finally, the qualification process of
the models with its merits and limits is considered and
examples of international experience are presented.

1. INTRODUCTION

Thermalhydraulics knowledge is one of the pillars of the
safe operation of nuclear power plants. This knowledge is the
result of many different research activities, (fundamentals,
experiments in small as well as large scale test facilities
and code development efforts) and must finally provide the
designer, utilities and regulatory bodies with tools (i.e.
codes) which are able to explain the phenomena and that
enhance the safety of the plant.

After years of development, the existing codes, (see for
instance ref. 1) are today already used over a wide range of
applications such as: licensing, support in concept and design
of plant systems, optimization of plant procedures and
setpoints, validation of real time simulation software,
incident analysis etc..

To be able to make a reliable calculation of a plant, not
only the codes but also the plant models, the user
capabilities and specific aspects of the given transient
should be addressed. This increases the complexity and
uncertainties besides those inherent in the codes themselves.
In fact, the uncertainties coming from the codes seem today easier to bracket than those from these other sources.

In considering those additional aspects, one should be aware that if the reactor is not tripped during the transient, the interplay between the automatic actions (controls, limitations and protections), and the neutronic/thermalhydraulic behaviour usually drive the dynamics. Even when the reactor trips, the initial conditions of the post trip period are determined by this interplay, which very often become an inevitable simulation problem for actual plants not present at the validation stages of the codes where most comparisons have been made with decoupled thermalhydraulic configurations.

It is the purpose of this paper to describe the problems met when trying to build a model to simulate an actual nuclear plant transient. As will be seen, most of the difficulties are of a practical nature calling for engineering rather than scientific approaches. User effects (ref. 2) and the code capability to handle the expected phenomena, although very important and closely related, will be only peripherally touched.

2 BASIC STRUCTURE OF PLANT MODELS

This section describes how a current TH code is able to model the different aspects of a plant transient simulation. To make a unified treatment, only one dimensional codes will be considered.

In most of the current 1D-TH codes, the code user has to build up a detailed noding scheme mapping the thermalhydraulic section to be analysed. With this nodalization approach he is defining an associated set of coupled equations that will require initial and boundary conditions to specify the system states.

This one dimensional TH-network is elaborated from a number of basic elements like volumes, (actually triggering a numerical algorithm for solving the two phase mass and energy conservation equations within the volume), junctions (the same for the momentum equations in between the volume in-out surfaces), pipes (a serial collection of volumes), valves (special solutions for local pressure drops due to section changes), pumps (usually a lumped equivalence to a local pressure gain calculated with experimentally adjusted two phase head to flow curves, but eventually an entire mechanistic pump model), tees (special provisions to account for 2D effects coming out of impingement between flows at different angles), heat structures (numerical algorithms for solving usually transverse to the flow one dimensional heat conduction equations with elaborated wall heat transfer packages and two phase flows or external environments as
boundary conditions) and other code dependent features, like separators, accumulators, pressurizers etc.

In addition, special modules handle non TH component processes like the reactor kinetics or the automatic actions of the control, limitation, protection and safeguards systems.

2.1. Scope of the simulation

An entire plant simulation would require a very large model to account for all coupling effects between the Auxiliary, the Balance of Plant and the Nuclear Steam Supply systems. As a result, the simulation is usually restricted to a part of the overall, and the model must draw a boundary around the system depending on the type of problem at hand. However, this implies that the coupling of the isolated part with the rest should be represented in some way. This may be a serious problem because "any partitioning, even with the use of coupling variables lagging one time step behind that allows for time step independent simulation of both sides, may lead to numerical dumping and phase shift (at best, in the absence of discontinuities; with discontinuities present, time lagging leads to nonsense)" (ref. 1).

The engineering approach usually tries to make the partitioning at points where experimental and/or operational information is available, using this information as boundary conditions. Because this is limited to a few situations, one may replace the out-of-boundary system, using appropriate assumptions, by simple relations between the coupling variables. A simple example is the assumption of symmetric "unfailed loops" behavior which allows to model one unfailed single loop with replicating boundary conditions for the others (ref. 2), or a single scaled loop representing the overall unfailed behavior.

This is easy to do in some frequent cases, for instance long term scenarios with early turbine and reactor trips and subsequent isolation of the feedwater secondary side system. In other cases a proper selection of the coupling variables can produce weak dependencies of the outside. An important example is the assumption of sonic conditions at some point downstream the turbine stages, which allows to correlate the turbine flow with impulse pressure and to decouple the system from downstream conditions (ref. 3).

Information about the time constants of subsystems is particularly useful in finding the appropriate assumptions. For instance, control systems of faster response than the time scale of interest may be replaced by the assumption that the control programs are satisfied instantaneously, as in the case of most turbine control systems.

More sophisticated techniques include the time step level connection of the thermalhydraulic code with a lumped model of
the outside (ref. 4), using either another code or special component subsystems that can be handled by the code itself (see section 2.3.4.). In many cases, linearization and use of collective transfer functions for the outside may be an excellent way of solving the problem at a minimum cost (ref. 5).

A typical example of the TH section of a three loop PWR model with the RELAP5 code is shown in fig 2.1. (this model has been set up with Tropic (ref. 6), a RELAP5 preprocessor developed at TRACTEBEL). This scope can be considered standard and limited to a) the primary coolant system, b) the pressurizer, c) the emergency core cooling system accumulators and d) the steam generators from the main feedwater isolation valves to the turbine stop valves. The important sinks and sources of water (letdown, charging, feedwater, safety injection, etc.) are handled as boundary conditions. Fig 3.2.2.2 shows a model with symmetric loop behavior for a similar plant.

2.2. Depth of simulation.

The degree of lumping of each component model and the nodalization scheme used to numerically solve the TH subsystem can be optimized considerably. However, the process is strongly dependant on the particular code and transient and much influenced by the purpose of the analysis that defines the optimization criterion (from scoping calculations where computer time saving is dominant to reproduction of an occurred incident where high fidelity is required). There are situations where a too detailed two fluid description may destabilize the calculation or make it less reliable than a lumped model if inadequate nodalization is used or because of lack of detailed input data (ref. 1).

In addition to general aspects, not specific for plant applications (too small cell size instabilities, particular nodalizations implicit in the development of empirical constitutive relations of the code, impact of cell size on time step -like Courant limit violations-, critical flow and 2D effects, large heat flux axial variations within the fluid transport time scale, ...) one should account for factors like:

i) Expected phenomena to occur during the transient (many transients are mild).

ii) Coupling with the time step required for control systems (particularly in the presence of discontinuities) and reactivity-neutron flux numerics.

iii) Complex hardware configurations with multiple flow paths that change both area and flow orientation, making it difficult to find equivalent 1D geometries.

iv) Sensor locations that activate automatic actions and force an increase in local detail which may however be
worthless with too coarse neighboring nodes.

v) Stiffness due to different time scales within the same transient (from the reactor alive and main pumps-off ATWS scenarios with scales dominated by reactor kinetics and natural convection, to reactor shutdown modes dominated by residual heat cooling time scales but sharp periods of protection interventions).

Unfortunately, in many cases, ad hoc solutions are inevitable. Usual techniques range from using already proved nodalizations for the same combination of expected phenomena and type of component, (which may be made systematic, see ref. 2, provided the range of values of relevant scaling parameter groups justify the use of experiment results) to "brute force" repetition of the calculations with several schemes to ensure consistency, particularly when handling items ii) to v).

Techniques have also been developed for specific codes like RELAP5. A recent approach is to optimize the grid, using results of the steady state, taking into account the specific features of the numerical scheme. More details are given in section 3.3. below.

2.3. Description of the model

2.3.1 The thermalhydraulic section.

This section of the model is constituted by a one-dimensional network of volumes, junctions, heat structures and the like. Fig 3.2.2.2 is a typical example of different TH subsystems of a PWR power plant.

The section is defined by the user by identifying i) length, free volume, flow area, hydraulic diameters, energy loss coefficients, surface roughness, etc. for the hydrodynamic part and ii) length, thickness, surface area and material composition with associated thermophysical properties like thermal conductivity and heat capacity for the heat structures.

The nodding and number of the heat structures depend on the transient (phenomena involving power generation under flow regimes and bulk fluid temperatures leading to low or quickly changing heat transfer coefficients require more detail), type of plant components (more sensible if high surface to volume ratios, as steam dryers and inner walls dividing flow paths at significantly different temperatures) and material composition, particularly high heat capacity components and/or heterogeneous layers of different thermal properties like fuel to cladding gaps.

2.3.2 The reactor section

This section of the plant model is covering the modules used to describe the thermal power generation within the fuel,
core coolant and core structures including fission power and residual heat generation and its spatial deposition. It is bounded by the modules that convert state variables into neutron cross sections -or reactivity- changes (henceforth reactor state variables).

Some of these reactor state variables are outputs of the thermal-hydraulic section like fuel and coolant temperatures and densities, while others are modified by control systems either in manual mode (simulated as external boundary conditions) or in automatic mode (outputs of the automatic actions section of the model, see below) like boron concentration or rod movements. Finally, others have a neutron flux dependent behavior like Xe and Sm concentration or burnout and can be considered as internal feedbacks within the reactor section or boundary conditions.

As can be seen, throughout the reactor state variables a strong coupling between the different sections of the model is established. Any best-estimate attempt to fully simulate such coupling poses a formidable problem, as it requires connection with spatial kinetics codes for transients where rod movements, as a result of control actions, and/or coolant density distribution changes may induce heat flux distribution transients (altering significantly the hydrodynamic behavior) that the code being used may not be able to predict.

It is then customary to take a decoupling approach similar to the one discussed in section 2.1. In standard PWR codes, the point kinetics model is usually available while in BWR 1D axial reactor kinetics is common.

As in other lumped models used, a major problem (ref. 1) is to ensure that the lumped parameters (in this case reactivity coefficients, rod, boron, Xe and the like, worths, as well as delayed fractions) are not too time dependent nor sensitive to the solutions of the equations, (so that they cannot be translated from one case to another) and that enough care is taken to ensure consistency between the parameter values and implicit assumptions involved versus the application made. It goes beyond our scope to further clarify this point, but in many cases the uncertainties and errors of this part of the process unbalance the quality of an otherwise reasonable TH calculation.

2.3.3 The instrumentation and control section

This section covers:

i) The simulation of the conversion of measured process variables into electronic signals (sensors and transmitters).

ii) The electronic processing devices (lead/lags, dead bands, hysteresis, filters, auctioneering elements, etc) that modulate the requirement for actuation of
any automatic action that has been included within the model.

iii) The modeling of the actuators, as for instance motors or mechanic devices that change control rods, valve positions or pump states, switches that align new incoming systems for protection/safeguard purposes, devices that trip components, etc.

iv) The connection of the actuator outputs to the other sections of the model, as valve position/geometry changes of TH junctions, control rod positions/cross sections or reactivity changes etc.

Commercial plants present a great variety of automatic actions that can be grouped into regulating (or control) actions that automatically run the facility, limitations that take action in case of malfunctions without tripping the reactor, like turbine runbacks or rod stops, reactor trips and safeguard actions that actuate in conjunction with trips and help to maintain the safety barriers as safety valves or emergency core cooling interventions. Fig 2.3.3 shows an example of the feedwater regulation system model of a two loop PWR plant indicating the transfer functions of some elements.

An accurate simulation of automatic actions is a must in any plant calculation (ref. 1). Not taking into account the feedback effects they induce through its interplay with the other sections of the model may lead to completely different scenarios. For instance, during a steam generator tube failure the feedwater control system will try to reduce the feedwater flow to the affected steam generator in order to maintain a programmed level. As another example, if the setpoint of the actuation of a protection action like a reactor trip is reached in the plant but not in the calculation or vice versa, the simulation is worthless.

In general the difficulties arise from gathering sufficiently detailed information of the peculiarities of the system, like sensor measuring principles and its range of applicability, saturation effects of control elements, the time delay they introduce, actual values of the setpoints and dynamic constants present in the plant at a given moment. The large size and variety of this model section in some designs (like KWU German plants) may be troublesome (ref. 2). This is alleviated by the fact that very simple approximations are available in cases of too slow or very fast action as compared with the dominant transient time scale, approximations that also help to resolve the stiffness problem mentioned in 2.2.v above.

In summary, sufficient knowledge of the automatic plant operation is a strong requirement to produce a model of reasonable size that does not omit essential aspects.
Figure 2.3.3. Feedwater regulation system model example.
2.3.4 The special component section

When defining the scope of the model, several equipment or components may be decided to be represented through a lumped system of usually ordinary differential (if linear, maybe by transfer functions), algebraic equations or boundary conditions instead of the TH field equations. Some codes provide these capabilities, usually limited. A better approach consists of coupling the TH codes with more specialized simulators (Ref. 1).

For instance, in RELAP5, equipment components such as containment, tanks, auxiliary, safeguard or balance of plant flow systems can be simulated using specific variables (called control variables) by creating equation sets that the code couples with the other sections. Fig 2.3.4 presents the high pressure safety injection system lumped description of the Doel 1 two loop plant as special components. The model simulates the four pumps feeding a common header connected to the primary circuit by four lines (one line per cold leg and two lines directly in the downcomer). The mass flow rates calculated by this lumped model are injected into the primary system through a special junction which is seen as a boundary condition.

A proportionate balance between the different sections of a plant model is a key factor in its quality, and the judicious selection of lumped versus detailed models is an important point to care. Too detailed simulation of some aspects combined with too rough descriptions of others that may be critical, degrade the best estimate effort in the TH side. Because the emphasis of this paper is in the two phase aspects, we will not go deeper in this direction even if it can never be stressed enough.

2.4. States of the system.

2.4.1 Steady states.

The solution of the plant model set of equations with zero time derivatives is called a steady state. There is a minimum number of variables (degrees of freedom) that entirely determine all steady state variables. This requires that necessary and sufficient information be supplied to the plant model, other than the data required to describe the system itself, to determine a given steady state. If less than required information is given, there may be an infinity of states. If more, most codes will ignore part of the information, in some cases without user control or else no state will be found. This point is often overlooked.

The situation is complicated because many codes, in order to avoid solving a different equation set for steady and transient cases, find the steady state as the final situation of a transient, called a null transient, that is triggered
Figure 2.3.4. Lumped description for the high pressure safety injection system
from detailed node by node initial conditions (usually non physical) suggested by the user. He or she imposes the required values of the degrees of freedom through boundary or initial conditions for those variables which are input to the code and use specific control system setpoints for those which are outputs, as for instance the power level.

The artificiality of this computation, which is accelerated by setting to zero part of the derivatives (i.e. zero heat capacities) may obscure to the user the essence of the final result, and force him to distinguish between initial conditions that are preimposed values of the degrees of freedom and those that are merely initial guess values that the code will change during the null transient. On the other hand, the method require criteria to determine from what time of the null transient the state can be considered steady and distinguished from self-oscillating states. One should also be aware that dead bands of control systems lead to somewhat undefined steady states and that hysteresis may produce different final states depending on the transient history.

In general terms, it is a delicate matter to identify the minimum number of degrees of freedom, particularly when control systems are strongly involved. In fact, the setpoints of the regulating systems and the external environmental conditions are in an ideal simulation the key variables, as it happens in an actual facility. However, additional variables are required which depend on the particular case because of:

i) the partitioning of the system in defining the scope of the simulation,

ii) the variety of possible lumped models in the core and special component sections,

iii) extra degrees of freedom, often introduced, that are actually system rather than state parameters.

For instance, any steady state of the point kinetics model requires zero reactivity, and the reactivity components coming out of the TH, automatic and special component sections should be compensated with external constant input signals (the excess reactivity). Some but not all codes provide for it, and the user should be aware.

By way of illustration, some rules of thumb can be given for defining the minimum degrees of freedom of the thermalhydraulic section if considered isolated. These include:

- two parameters per single phase fluid entering the system; and five per two-phase fluid.

- one parameter (typically, the outlet pressure) per fluid leaving the system, unless choked flow exists.

- one parameter per heat structure bounded by only one fluid (typically, the outer side temperature); and one more if
the structure has non-zero internal power generation.

- one parameter per valve (e.g. its position) and per pump (its speed).

2.4.2 Self-exciting states

Due to the strong nonlinearities of the model, mainly those introduced from control systems (hysteresis, dead bands, on-off etc) but also from hydrodynamic and neutronic processes, (like BWR plants under natural convection), there is no guarantee that the permanent regime (i.e. asymptotic time behavior) after a null or physical transient will be a steady state. Although uncommon, self-oscillating states either periodic or non periodic are possible and the problem arises to know whether they simulate actual states of the plants or they are the result of purely numerical oscillations.

2.4.3 Transient states

In most cases the initial conditions for transients are steady state situations, and the transient states will be defined by supplementing the steady state with time dependent boundary conditions in appropriate variables, in a number totaling the degrees of freedom of the steady state, (eventually including constant time histories for some of them). Usually, they are variables governed by conditions outside the problem boundaries; some are simple (like pressures or temperatures of the fluid in the reload storage tank) but others are complex (like the containment pressure in a large LOCA). In the latter case, an iteration process with another code may be necessary (the containment pressure impacts the peak cladding temperatures).

In view of the lack of detail, it may be necessary to optimize the response by changing additional parameters not influencing the steady but the transient states, like discharge coefficients or relief or safety valve capacities. However, this practice artificially increases the degrees of freedom and the values obtained for those coefficients may be transient state dependent and cannot be extrapolated.

3. PLANT CALCULATIONS AND CODE ASSESSMENT

In this section we examine the problems involved in extracting full scale information from plant measurements to complete and verify the validation of the mathematical representation of two phase thermalhydraulic phenomena included in the TH modules of the so called best estimate codes.

The use of data from commercial power plants offers a major advantage for code assessment since all distortions in
geometry, pressure or temperature are eliminated. On the other hand, it has been proven in some international exercises that the results could be noding sensitive and that nodalization schemes may have been adapted to fit the experimental results. Code assessment on real plants is then very useful to address the nodalization issue. These advantages, however, are strongly limited by the problems we discuss in the following sections.

3.1. The measurement problems.

Table 3.1 shows an example of the nature of the parameters which are generally available on plant recorders of a typical PWR-W plant. Many instruments are related to the operation of auxiliary systems. Common instrumentation in test rigs, like gamma densitometers local delta-p or thermocouples within heat structures are not available, and even for those existing the range may not cover the whole transient. Also, the quality of the data may be rather poor as the instrumentation is selected more on the basis of robustness and reliability than accuracy, particularly for old plants. Finally, the low recording frequency eliminates many instruments during a transient, particularly the neutron flux local instrumentation that may decalibrate. Other deficiencies include uncertainties in exact sensor location (position, inclination, depth of insertion,...) and obscure or unavailable to the analyst calibration sheets and procedures.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Range</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Requested position of group 4</td>
<td>0-230</td>
<td>Step</td>
</tr>
<tr>
<td>Reference temperature</td>
<td>270-330</td>
<td>°C</td>
</tr>
<tr>
<td>Primary pressure</td>
<td>0-200</td>
<td>bar</td>
</tr>
<tr>
<td>Required charge flow</td>
<td>0-100</td>
<td>m³/h</td>
</tr>
<tr>
<td>Source range flux detector</td>
<td>1-6</td>
<td>c/s</td>
</tr>
<tr>
<td>Control valve CC234</td>
<td>0-100</td>
<td>%</td>
</tr>
<tr>
<td>Condenser vacuum</td>
<td>0-1</td>
<td>bar</td>
</tr>
<tr>
<td>Flow rate in heat exchanger 15</td>
<td>0-120</td>
<td>%</td>
</tr>
<tr>
<td>Control rod speed</td>
<td>6-72</td>
<td>step/min</td>
</tr>
<tr>
<td>Nuclear power</td>
<td>0-120</td>
<td>%</td>
</tr>
<tr>
<td>Averaged temperature</td>
<td>270-330</td>
<td>°C</td>
</tr>
<tr>
<td>Steam collector pressure</td>
<td>0-85</td>
<td>bar</td>
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<td>Current meter H7</td>
<td>0-100</td>
<td>%</td>
</tr>
<tr>
<td>CV tank level</td>
<td>0-100</td>
<td>%</td>
</tr>
<tr>
<td>Demineralised water flow</td>
<td>0-65</td>
<td>m³/h</td>
</tr>
<tr>
<td>Position valve between EX16 to 15</td>
<td>0-100</td>
<td>%</td>
</tr>
</tbody>
</table>

Table 3.1.

Some parameters usually available on plant recorders

With the advances of numerical techniques in data gathering, plant dedicated digital data acquisition systems
can enhance considerably the data quality. Most modern units are equipped with such standby systems triggered by a logic signal from the plant protection system.

On the other hand, the range of TH variable values that may be involved in actual transients is obviously restricted for safety reasons and the potential for possible checks of many important phenomena is rather limited.

Finally, the local nature of the sensors forces a detailed simulation of the detector region; in addition to the problems mentioned in 2.2.iv there are other details like filtering effects, voltage conversions, signal limitations, etc., that require detailed engineering knowledge.

3.2. The global picture problem

Because only global measurements like flows, pressures, levels and a few local variables, mainly fluid temperatures are available, and even if decoupling of the TH section is successful, (see below) the comparison of results may be masked by two major reasons:

- First, the inherent stability of two phase phenomena tends to compensate the different components entering into the global result.
- Second, in many cases a portion of the measured information has already been used to adjust unknown parameters that play an important role (section 4.4).

Therefore, considerable analysis is required before a particular deviation or agreement can be attributed to a particular TH model. We detail below this point in the important case of pressure and level (all plants are reasonably equipped for its measure and many automatic actions are triggered on setpoints of these variables).

3.2.1 Pressure analysis

To better understand the aspects of the two phase flow modeling involved in the evolution of the global system pressure, we will write an overall volume balance that can be derived from the basic mass and energy conservation equations. (ref. 1 and ref. 2). The message is that pressure evolution is the result of the accommodation by the system of the net excess fluid volume during the transient, as a net effect of different expansion/contraction mechanisms and the injection/extraction of volume through the external boundaries. This net volume to be accommodated is converted into a rate of pressure change through the global isentropic compressibility of the system, that plays then an essential role.
With the standard notation \( \alpha, v, \rho \) for void fraction, velocity and density, \( \Gamma, T, p \) for the vapor generation rate, temperature and pressure, \( h, h_s \) for bulk and saturated enthalpies, of a node of section \( A \) of a two phase liquid (\( \omega_1 \)) and vapor (\( \omega_2 \)) subsystem of total \( V \) and \( H \) volume and height, the balance reads:

\[
(C) \quad \text{(Compressibility)} \ast \frac{1}{<p>} \frac{d<p>}{dt} = \text{volumetric flow in} - \text{volumetric flow out} + \text{expansion/contraction due to phase change} + \text{exp./contract. of liquid due to bulk enthalpy change} + \text{exp./contract. of vapor due to bulk enthalpy change} + \text{acoustic effects due to pressure gradients}
\]

where the different terms are defined per unit system volume and have dimensions of time constants.

They are linked to the TH models through the isobaric expansion and isentropic coefficients

\[
\pi_k = \left[ \frac{\delta(1/\rho)}{\delta h} \right]_p, \quad \gamma_k = \left[ \frac{\delta \ln p}{\delta \ln \rho_k} \right]_S
\]

in the following way:

**System Compressibility**

\[
C \text{ term: } \frac{1}{V} \sum_k \int A(z) \frac{\alpha_k(z,t)}{\gamma_k(z,t)} \, dz
\]
In-out volumetric flow change

\[ J \text{ term: } \frac{1}{V} \sum (V_{in} \alpha_{in} A_{in}) - \frac{1}{V} \sum (V_{out} \alpha_{out} A_{out}) \]

Change of phase expansion/contraction

\[ \Gamma \text{ term: } \frac{1}{V} \sum_k \int_0^H \Gamma_k A \left[ \frac{1}{P_k} + \pi_k (h_{k_s} - h_k) \right] \]

\[ \Gamma_q = -\Gamma_1 = \Gamma \]

Phase expansion/contraction due to bulk enthalpy changes

\[ L \text{ term: } \frac{1}{V} \int_0^H d\pi_1 \left[ -\int_j \left. \frac{dS}{d\rho} + \alpha_1 Q'''' \right|_{P_{w1}} + \frac{1}{V} \int_0^H d\pi_1 \left[ -\int_j \left. \frac{dS}{d\rho} + \text{DISS}_1 \right|_{P_{w1}} \right] \]

\[ G \text{ term: } \frac{1}{V} \int_0^H d\pi_q \left[ -\int_j \left. \frac{dS}{d\rho} + \alpha_q Q'''' \right|_{P_{wq}} + \frac{1}{V} \int_0^H d\pi_q \left[ -\int_j \left. \frac{dS}{d\rho} + \text{DISS}_q \right|_{P_{wq}} \right] \]

where

\[ -\int_j \left. \frac{dS}{d\rho} \right|_{P_{w}} \]

is the interphase linear heat generation rate of the \((k=1,q)\)
liquid and vapor phases, (similar notation for wall heat) and
\(Q''''\), DISS are the volumetric heat and fluid dissipation.
Finally

Acoustic effect

\[ A \text{ term: } \int_0^H dz \left( \ln \left( \frac{P}{P_0} \right) \frac{\partial C(z,t)}{\partial t} \right) - (C_{v1} + C_{v_q}) \frac{\partial \ln \rho}{\partial z} \]

with \(C(z,t) = C_{v1} + C_{v_q}\) the local compressibility.
The global or system pressure \( p \) to which this balance is associated, is a compressibility averaged over the volume of the system, that allows to talk about system pressure with no reference to any particular location, but it may not coincide with the local value of the sensor tap (see 3.1).

Because of the one to two orders of magnitude difference between the expansion and isentropic coefficients for the vapor and liquid phases, there is a strong relation between level (see next section) and compressibility and in subsystems like BWR vessels, steam generators and pressurizers, (in states with level values within the range where the vapor and liquid volume are of the same order) the vapor often dominates the behavior. Terms \( J \), \( \Gamma \) and the compressibility are large and the major factors influencing pressure are then the net steam volumetric flow together with the total vapor generation rate, that originate the gross portion of the volume change. The high compressibility reduce the pressure rates. Within this general pattern one should be aware that cancellations occur between these large main terms, and the \( G \) term may become important (see the pressurizer example below) for high pressure rates.

On the contrary, low void systems, like the primary side of a PWR excluding the pressurizer, are dominated by the net liquid volumetric flow (CVCS charging-letdown and surge line flows), and the liquid expansion/contraction. These terms are now small and the also small compressibility amplifies the pressure rates. Again cancellation may occur between the core expansion and SG primary side contraction. Finally, if the pressurizer is also included in the subsystem, the surge line flow disappears in the \( J \) component, being replaced by the pressurizer terms, and all contributions to the balance are of the same order.

This balance can also be used with models involving simplifying assumptions, in order to compare the same quantities in the different cases. For instance if one phase is assumed at saturation, the corresponding \( L \) or \( G \) term in the balance becomes basically proportional to the \( C \) term. If two phases are at saturation (thermal equilibrium), in addition, the \( \Gamma \) term reduces to a flashing component (proportional to the \( C \) term) plus the ratio of the external heat flux to the latent heat typical of homogeneous models. Even these homogeneous models essentially derived from single fluid approaches can be formulated in this scheme because they are equivalent to the thermal and mechanical equilibrium assumption in two fluid theories. Once the balance is performed, if the above relationships between components are demonstrated, a good agreement of pressure calcs versus measurements would not be enough to discriminate between different models and the assessment would be inconclusive from that point of view.

As an example, in fig. 3.2.1.1.a and 3.2.1.1.b we present results of a turbine-reactor trip secondary side pressure
Figure 1.2.1.1. Results of a Turbine Trip in a PWR-W Plant. Steam Generator Secondary Side Pressure Evolution.
evolution of a 3 loop Spanish Westinghouse plant equipped with a modern data acquisition system. The nodalization is included in fig 3.2.2.2.A. The transient was analyzed with the RELAP5/MOD2 code and then postprocessed (ref. 3, ref. 4) to evaluate the results off-line. The acoustic effects have been bounded as error bands and in the post calculation of the balance terms we have assumed the emerging phase at saturation at every node.

The figures show the major factors at each stage of the pressure evolution dominated entirely by the in-out flow and change of phase. Because the first is controlled by the steam dump and relief valves, the appropriate simulation of valve capacities, control system and actuators response, discharge coefficients, etc., are then of paramount importance. Fig 3.2.1.1.a shows the RELAP and postprocessed system pressure together with the plant records.

In fig 3.2.1.1.c. we present the components of the overall expansion due to bulk enthalpy changes, where we have split the terms associated to the interphase (internal) and wall (external) heat transfer, also indicating the cancellations between themselves.

The equivalent balance of the pressurizer decoupled from the primary side under the same transient is shown in fig 3.2.1.2.b. It can be seen that in this case a vapor expansion spike subsequently replaced by another flashing spike partially compensates the outurge volumetric flow. The discrepancy observed between RELAP and its postprocessed curve, was due to a very fine spike of the vapor generation rate not sampled in the postprocessing. Further analysis indicated a problem in the RELAP5 correlation of the interfacial area where a factor \( (\nu/\nu_i)^2 \) dependence resulted in very high \( \Gamma \) values under counter-current pressurizer transient flow regimes, showing potential code deficiencies.

In summary, only after a detailed study one can justify a particular deviation of pressure results or the implications of a good agreement on the validation of particular aspects of models which, as shown before, may be entirely pressure insensitive in a given scenario. Note that the balance also allows to define scaling groups (ref. 5 and section 4.4.3).

3.2.2. Level analysis

Although level is usually measured as a properly compensated pressure difference between taps at two different axial locations where phase separation is expected, in the absence of dynamic local momentum effects it may be related with the volume enclosed by the liquid phase, \( V \), within a prespecified subsystem, of volume \( V \) like certain regions of the pressurizer, SG or BWR vessel dome. For instance, the subsystem of fig. 3.2.2.2.C is an appropriate volume for wide range SG level measurement in the turbine trip scenario. In
Figure 3.2.1.2. Results of a Turbine Trip in a PWR-W Plant. Pressurizer Pressure Evolution.
general then
\[ V = \phi\left(\frac{V}{V'}\right), a_j, x_k \]

where we distinguish time independent parameters mostly geometrical with the symbol \( a_j \) and separate the important time dependent variable \( V/V' \) from others \( x_k \) on which there will be a weaker dependence (dynamic corrections). The shape of the \( \phi \) function may be dictated by geometric detector requirements and environmental conditions and may be strongly non linear if so is the axial profile of the cross section within the definition volume \( V \). In fig 3.2.2.1.a,b. we show an example of the effect of dynamic corrections in a BWR/6 calculation (ref. 1).

In order to understand the liquid volume evolution, a volume balance can also be written, this time for each of the phases.

\[ \frac{dV(t)}{dt} = (J)_1 + (\Gamma)_1 + L - (C)_1 = -[(J)_q + (\Gamma)_q + G - (C)_q] \]

where the notation stands for the corresponding phasic terms of the pressure balance and where we have omitted the acoustic effects. The \( C \) terms show another relation between level and pressure. Note that once the level control system is successful, an additional constraint between the terms governing the pressure behavior is imposed, which is independent of the two phase peculiarities of the different TH models.

In fig 3.2.2.3.b we present the vapor volume balance in the subsystem of fig 3.2.2.2.C for the same transient of section 3.2.1. The pressure term includes the \( G \) and \( C \) terms together. The balance suggests the following explanation of the reactor trip RELAP5 level transient:

Subsequent to the cut of the volumetric flow out due to the turbine trip, the boiler vapor generation produced a SG dome excess steam volume, partially accommodated via the pressure transient and steam dump opening. The initially high recirculation and feedwater flow entrained the steam and a two phase non separated mixture was formed in the downcomer below the feedwater nozzle. Steam dump progressively closed, and level was temporarily recovered by steam condensation (fig 3.2.2.3.d) in the downcomer due to the feedwater spray effect, until feedwater isolation and the loss of the recirculation flow induced downcomer phase separation. The condensation rate then dropped to a value (regulated by the spray effect of the incoming auxiliary feedwater) that the boiler vapor generation was able to balance. We see in fig 3.2.2.3.c the evolution of the steam volume and in fig 3.2.2.3.a the good match of the RELAP5 final result.
Figure 3.2.2.1. Effect of sensors in level comparisons in a BWR plant calculation.
Fig. 3.2.22

Nodalisation of different TH subsystems of a PWR plant.
Figure 3.2.2.3. Results of a Turbine Trip in a PWR-M plant. Steam Generator Secondary Side Level Evolution.
Figure 3.2. A. Results of a Turbine Trip in a PWR Plant. Pressurizer Level Evolution.
Section 5.1.3 presents the assessment conclusions of this SG level analysis together with a parallel experience in another PWR.

Finally in fig 3.2.2.4 we also give the equivalent result for the pressurizer.

3.3. The nodalization problem.

For assessment purposes, it is very convenient to take into account the specific numerical scheme of the code to optimize the nodalization. As an example, a program that runs on a PC has been recently developed to allow the determination of component nodalization length for RELAP5 (Ref. 1). In short, the program is based on a characteristics analysis of the governing equations, and the application of the Von Neumann local stability theorem (Ref. 2) to the code semi-implicit formulation. From this analysis stem criteria for numerical stability that support the nodalization process.

Other methods provide criteria to select between different schemes. For instance, because the derivation of the volume balance described before is a rigorous consequence of the field equations and independent of the subsystem and of any particular numerical scheme, its verification with the numerical solution, as found by the code at the nodding level, is a good check of the nodalization quality.

3.4. Decoupling two-phase flow subsystems.

We have shown that isolation of a TH subsystem may allow to assess experimental pressure and level results. However, only if measured information is available about in-out volumetric flows and the heat generation/environmental conditions of the heat structures (that actually carry the influence in pressure and level of the other sections of the model), the results can be made reliable. Otherwise, any discrepancy can be due to so many factors that it may be hopeless to obtain conclusions. With the usual type of instruments available, this information (except for ad-hoc specific tests with additional instrumentation) can be expected only at points where the system is in single phase and flow/temperature sensors do exist. This requirement is very restrictive. For instance, steam generators can be decoupled in most scenarios, pressurizers only with restrictions and BWR vessels after reactor trips.

4. PLANT CALCULATIONS AND MODEL QUALIFICATION.

By model qualification we understand the process that produces a model such that:

i) the parameters requiring empirical (i.e. based on
measures) identification (ref. 1) are not sensitive to the transient or set of transients that were used to identify them,

ii) the number of such parameters is minimum and they have physical meaning,

iii) the range of scenarios that the model is able to cover without parameter readjusting or model changing is maximum and well defined, and

iv) the level of detail is proportionate to the extent of the quality guaranteed data base and to the expected application.

In general, the qualification procedure should consider four steps, namely qualification of a) the plant data base b) the input deck c) steady state fingerprints and d) the dynamic states.

4.1. Plant data acquisition.

The basic information is painful to obtain. Leaving aside confidentiality aspects, conflicting values often appear from different sources like non best estimate design and safety analysis versus up to date as built or measured data. One example is the primary cooling flow where several values are used for different design purposes. Overall coherence is indeed difficult and results from cooperation of different teams. Therefore a documentation system able to trace back each piece of the data set is of paramount importance.

4.2. Input data qualification.

Once decisions have been made (and also documented) concerning the problems described in section 2, a complete input is produced using the plant data base. It is recommended to be ambitious in the scope, depth, etc., to prevent that the peculiarities of the transient motivating the calculation induce a model that will require work duplication for another transient. It is advisable that this data deck, after some steady state fingerprint, be distributed to independent experts in the different model sections to prevent data misunderstanding and ensure actualization.

Before running the code, global self-checking for consistency is useful. For instance, comparison of the volumes and masses for the primary system and steam generators between data used in the code and the global data available from vendor documents.

4.3. Steady state verification.

Basically all plants have a reasonable recording of steady state values of variables in a number beyond the state degrees of freedom and at different power levels, particularly
those obtained during startup. This information is very useful, if properly handled, because at least a portion of the extra variables can be predicted by the model.

It is inevitable that some system (rather than state) parameters like fouling and friction factors, or equivalent monodimensional geometries, be identified using some of the extra measurements. An index of the model quality is then the relative number of measurements that are actually predicted.

In an ideal situation one could use a reference power level for fingerprint purposes and then through a set of null transients properly controlled, predict the extra variables at another power level without further parameter adjusting. Difficulties may arise from lack of exact information of the plant status, particularly the core, auxiliary and control systems. Usually, a compromise is needed and a few parameters are adjusted at different power levels, the rest of the extra measures being predicted.

For instance, steady states predicted at different power levels by the BNL Engineering Plant Analyzer in a BWR/6 (ref. 1) are shown in fig 4.3 in the operating map. In this map the locus of points corresponding to a given control rod configuration is a slightly bent curve. The initial and final conditions of a recirculation pump high to low speed transition startup test were used to adjust for losses upstream and downstream the core. Changing only the recirculation flow one finds the simulated operation map line that has a higher slope at the test than the measured conditions of the actual operation map due to Xenon non-equilibrium conditions.

4.4. Dynamic verification.

To a lesser extent, the comments before may be applied for transient states although only a few additional fingerprints are expected (unless the setpoints and details of the automatic section are not well known), typically those related with systems actuated by controls, limitations and protections like safety and relief valve capacities and discharge coefficients. We have already shown that, if only global results like pressures will be compared, any adjusting of this kind may be inadequate and the quality can only be judged after a postprocessing analysis. Another important aspect concerns the reactor state coupling parameters. Although some measurements are made as part of the cycle startup, the extrapolation of these measurements to transient situations is very delicate and may force to identify some of these parameters with transient results.

4.4.1 Startup tests.

The process of building a model may be envisaged as paralleling that of the system integration phase of building the plant itself. This emphasizes the potential of the
operational startup to obtain dynamic information, the convenience of qualifying the plant model by subsystems (ref. 1) and the importance of having a good data acquisition system. In table 4.4.1 we sample a few of those tests for W and GE plants, indicating the model section that can be qualified. It is important to realize that any identification of parameters made should check aspect 4.1) for quality.

Although more important in the case of incidents, a good record of operator interventions during startup tests is also a must.

4.4.2. Equipment trips and unexpected incidents.

Similarly, some frequent equipment trips that supply information are given in table 4.4.2.1. for the same designs including those which are current startup tests. They are a good complement to those in 4.4.1. For plants that trip the reactor as a result of main equipment loss, a lot of the automatic, reactor, and special components section become inactive giving the opportunity to qualify the TH section. For plants with idle capability these measured transients can be a serious confirmation of the whole model, depending, as always, on the degree of parameter adjusting and the quality of the postprocessing. Although more undefined, unexpected events may be a good source for phenomena in different than usual scaling ranges. Finally in case of incidents not caused by nor equivalent to equipment trips, operator interventions are essential and difficult to ensure in its detailed sequence. In Table 4.4.2.2. we show a qualification matrix of a PWR Plant (ref. 1).

4.4.3. Accidental situations.

Many important thermalhydraulic phenomena are never expected to occur or have very low probability and resource to scaling is inevitable. It is not within our scope to detail the intensive work being made today in this area. (See for instance ref. 1, ref. 2).

The basic idea is to associate to a given scenario in the real plant, a scaled experiment defined through a set of nondimensional parameter combinations or scaling groups, such that the phenomena predicted by the code to occur in the real plant are in the same range for the scaled experiment.

For instance, a L081 experiment was designed (ref. 3) to qualify the RELAP5/Mod2 code and the Jose Cabrera plant model for single tube steam generator tube ruptures. The transient was first analysed with the plant model. All the main plant parameters were scaled to match the steady state. Fig 4.4.3 shows the predicted evolution of the pressurizer level in the plant and in the test facility together with the experimental results.
<table>
<thead>
<tr>
<th>Plant Type</th>
<th>Plant Condition</th>
<th>Tests</th>
<th>Aspects to be qualified</th>
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<tbody>
<tr>
<td>PWR-U</td>
<td>Hot Standby</td>
<td>Measurement of RCS flow</td>
<td>Pump model, friction factors and flow losses in the RCS (single phase)</td>
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<td></td>
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<td>RCS flow coast-down under pump trip</td>
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<td>Low power</td>
<td>RCS pump power measurement</td>
<td>Heat sources other than reactor power</td>
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<td>Rod worth measurements</td>
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<td>Reactivity coefficients measurements</td>
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<td>Natural circulation tests</td>
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<td>Intermediate and High Power</td>
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<td>Reactor-Theranuclear coupling</td>
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<td>Steam generator level control system</td>
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<td>Shut-down margin and criticality</td>
<td>Reactor kinetics model</td>
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<tr>
<td></td>
<td>Different power and core flow levels</td>
<td>Pressure regulator, recirculation control system</td>
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<td></td>
<td></td>
<td>Feeder/level control system</td>
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<tr>
<td></td>
<td>High power and high core flow</td>
<td>Power behavior as a function of void fraction (system response to control rod movement and pressure regulator changes)</td>
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</tr>
</tbody>
</table>

Table 4.4.1. Sample start-up tests useful for model qualification (equipment trips not included).

<table>
<thead>
<tr>
<th>Plant Type</th>
<th>Plant Condition</th>
<th>Tests</th>
<th>Aspects to be qualified</th>
</tr>
</thead>
<tbody>
<tr>
<td>PWR-U</td>
<td>Full power</td>
<td>Turbine trip</td>
<td>Integral model verification</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Single feedwater pump trip, turbine runback</td>
<td>Control and Limitation systems</td>
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<tr>
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<td></td>
<td>Loss of main feedwater</td>
<td>Control and Protection systems</td>
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<tr>
<td></td>
<td></td>
<td>Rod drop and reactor trip</td>
<td>Protection system, Reactor and Thermanuclear coupling</td>
</tr>
<tr>
<td></td>
<td>Start-up tests</td>
<td>Full power</td>
<td>Generator trip without turbine trip</td>
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<tr>
<td>BWR-CE (BWR-4)</td>
<td>Full power</td>
<td>Turbine trip</td>
<td>Integral model verification</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Single feedwater pump trip, recirculation runback</td>
<td>Control and Limitation systems</td>
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<tr>
<td></td>
<td></td>
<td>Loss of main feedwater</td>
<td>Control and Protection systems</td>
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<tr>
<td></td>
<td></td>
<td>Single or double recirculation pump trip</td>
<td>Reactor, Thermanuclear and Control systems</td>
</tr>
<tr>
<td></td>
<td>Start-up tests</td>
<td>Full power</td>
<td>Generator trip without turbine trip</td>
</tr>
</tbody>
</table>

Table 4.4.2.1. Sample equipment trip induced transients useful for model qualification.
<table>
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</table>

List of systems:
1. Reactor Trip System
2. Safety Injection System
3. Turbine Trip System
4. Main Feedwater Trip System
5. Auxiliary Feedwater System
6. Steam Dump Control System
7. Turbine Control System
8. Pressurizer Level and Pressure Control System
9. Feedwater Control System
10. Average Temperature Control System
11. Secondary Relief Valves
12. Secondary Safety Valves
13. Pressurizer Spray
14. Primary Relief Valves
15. Primary Safety Valves

List of transients:
.a. Station Blackout
b. Faulty Pressurizer Spray Valve Opening
c. Turbine Trip without Steam Dump and Secondary Relief Valves available.
d. Loss of Feedwater (LOFW)
e. Turbine Trip with all Systems Available
f. Turbine Power Step

<table>
<thead>
<tr>
<th>TECHNATOM</th>
<th>TRACTEBEL</th>
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<tbody>
<tr>
<td>(by courtesy of Tecnatom SA)</td>
<td>(by courtesy of Tecnatom SA)</td>
</tr>
<tr>
<td>Turbine trip</td>
<td>Turbine trip, loss of F.W. and delayed reactor trip</td>
</tr>
<tr>
<td>ATWS with loss of feedwater</td>
<td>Loss of feedwater - ATWS</td>
</tr>
<tr>
<td>RCP locked rotor</td>
<td>Shaft seizure in RCP with delayed reactor trip</td>
</tr>
<tr>
<td>Cold leg small break</td>
<td>Three inch LOCA at bottom of cold primary loop</td>
</tr>
<tr>
<td>S.G. tube rupture</td>
<td>S.G. tube rupture (3 tubes)</td>
</tr>
<tr>
<td>Pressurizer relief valve opening</td>
<td>Loss of reactor coolant flow and inadvertent depressurization</td>
</tr>
<tr>
<td>TMI-2 accident</td>
<td>Three Mile Island accident scenario</td>
</tr>
<tr>
<td>Total loss of feedwater</td>
<td>Steam line break inside containment</td>
</tr>
<tr>
<td></td>
<td>Controlled emptying of RCS (natural circulation)</td>
</tr>
</tbody>
</table>

Table 4.4.22.
QUALIFICATION MATRIX OF RELAP5/MOD2 MODEL OF ASCO N.P.P.

Table 4.4.3. Validation matrices for training simulators.
This technique can also be used to establish a qualification matrix that includes all the dynamic verification steps here presented, and oriented toward a specific application. In some sense this is an extension of the code validation matrices but applied to plant model verification. For instance, in table 4.4.3 we present examples of qualification matrices used for validating a training simulator (ref. 4 y ref. 5).

4.5. Is a single model possible for all transients? for all applications?

The scaling approach can also be useful to answer this important practical question, as it may indicate to which extent the verification process has included all phenomena of interest. However, the answer is complicated by the many factors discussed in section 2 including nodalization dependence, boundary conditions, degree of lumping, model section interplay, 2 and 3D problems etc, and today it seems difficult to see how this can be done. In addition, certain applications like design review require a specific methodology which may be very difficult to follow with a single model.

Broadly speaking, models for events with reactor and turbine alive can be expected to cover up to small break scenarios, while events with tripped reactor and turbine but large breaks involved would require a different model.

For instance fig 4.5 shows the model of the Jose Cabrera NPP steam generator secondary side. The moisture separators and dryers are modeled in detail and so are the liquid paths of return to the downcomer. This model works appropriately in a wide range of transients and certain accidents, but gives wrong results in accidents like a double side break of the main steam line (ref. 1).

5. EXAMPLES

5.1 Operating experience

5.1.1 Influence of unknown data in heat structures

Figs. 5.1.1.a and 5.1.1.b show the results obtained with RELAP5 relative to a DOEL 3 startup scram test at beginning of life conditions where the decay heat is particularly sensitive to the previous operating history. They correspond to two different assumptions on the exact power history (ref. 1).

Similarly stored energy may notably contribute as a potential source of uncertainty even for transients not involving breaks. Fig 5.1.1.c shows the impact of the use of
two different values of gap conductance (BOL and MOL) on the TRAC PF1 simulation of pressurizer level in a main steam isolation valve transient occurred in Ringhals 2 (ref. 2).

![Graph showing pressurizer level over time with different markers for filtered, unfiltered, modified H-GAP, and measured data.]

Figure 5.1.1.c. Ringhals-2 Steam-line Isolation Valve Closure. Pressurizer Level.

5.1.2 Influence of unknown control system status.

In the RELAP5 simulation of a DOEL 3 load rejection test, a pressurizer pressure discrepancy was found in the base case calculation from 120 sec as shown in fig 5.1.2.a. Lack of indication about the status of the backup heaters and doubts about some pressure control lead-lag parameter values suggested to repeat the analysis assuming these heaters on between 125 and 490 sec. Fig. 5.1.2.b shows a considerable improvement in the result (ref. 1).

5.1.3 Influence of a code deficiency.

Similarly, in a DOEL 4 reactor trip simulation (ref. 1), an excessive decrease in SG narrow range level has been observed. Through several sensitivity studies this discrepancy was attributed to a lack of initial mass inventory in the steam generator which could be related to a well known deficiency (ref. 2) in the RELAP version then in use of the interphase drag model. Fig 5.1.3 a. shows the evolution of the SG water level. The discrepancy between the calculated and the measured level evolution (at the end of the transient) vanishes when the initial mass inventory has been artificially increased as shown on fig. 5.1.3.b.
Figure 5.2.1. RELAP-5 results for a DOE-L-1 start-up scram test.

Figure 5.2.2. RELAP-5 results for a DOE-L-1 start-up load rejection test.
The same was experienced independently (ref. 3) in the simulation of the Vandello's 2 PWR turbine trip discussed in section 3.2. The dependence of the final SG level on the initial liquid volume in the boiler can be easily seen by time integrating the level volume balance for the dome and boiler separately, and accounting for the fact that at the end of the transient both levels should be the same because the recirculation is lost. Finally, the relationship of the lower boiler initial liquid inventory with the interphase drag was confirmed by comparing the axial void profiles of the steady states of RELAP5 MOD2 and MOD3 for the same boiler exit qualities (fig 5.1.3.c).

5.1.4 Influence of appropriate model boundary closure

The turbine can be modeled in different ways depending on the transient to be analysed or on the data available to build a special subsystem. In the same exercise as described in 5.1.2 a sensitivity study was carried out. In the time period between 50 and 600 sec, the equivalent valve cross section varies from 11% to 9.9% in the base case, while this section is kept constant at 9.6% in the parametric study. The impact on the pressurizer level is shown on fig 5.1.4.a for the base case and on fig 5.1.4.b for the parametric case. This illustrates the importance of a good modeling of some equipments which could be judged less important when defining the scope of the model.

5.1.5 Influence of actuators response

In the RELAP5 simulation of the Doel reactor test trip described in 5.1.1, a parametric study has been carried out in order to check the influence of the steam dump valve closing speed (this parameter is generally not well known). The faster closing in the steam dump valves assumed in this parametric study had a marked effect on the plant cooldown which is interrupted too soon. The primary temperature, (fig 5.1.5.b) drops to a minimum value which remains 1 degree C higher than in the reference case (fig 5.1.5.a).

The subsequent heat-up phase starts from a higher temperature and reaches therefore the no load reference temperature nearly 1 minute sooner than measured. As a consequence of this less important cooldown the feedwater trip signal or low advanced cold leg temperature is not reached.

5.1.6 Influence of the BOP simulation

The effect of a long term stack open safety/relief valve in a BWR was analysed with the BNL Engineering Plant Analyzer (ref. 1). One of the objectives was to determine whether the final power level would be above or below the initial one. The analysis showed that the effect of feedwater temperature cannot be ignored. If it is performed with constant temperature (lack of operational data makes difficult to impose more detailed boundary conditions) the conclusions
would be the opposite.

5.1.7 Influence of time step in control systems

Following is one conclusion of the TRAC PF1 analysis of section 5.1.1:

"For this kind of fairly mild transients no problem with the TH section is expected. Instead the control system performance becomes a source of difficulty. Due to the explicitness of the control system processing, it is clear that the control performance is sensitive to the calculational timestep, thus becoming a limiting factor" (Fig 5.1.7).

5.2 Lessons learned from incident analysis

As a result of the international standard problem ISP20 (DOEL 2 steam generator tube rupture, ref. 1) several conclusions were obtained that illustrate several aspects presented in the previous chapters. They are reproduced here:

1 "This ISP clearly illustrates the special problems related to simulating transient behavior of a real power plant: limited access to precise plant data, lack of precise knowledge of all sources and sinks of mass and lack of heat signal interpretation".

2 "Most participants succeeded in reaching an acceptable simulation of the primary system parameters while for the steam generator parameters a large spread in the results was noticed. For the RELAP code this large spread must be attributed to the user effect since modifications were introduced in the reference RELAP5 data deck available for this ISP (form loss factor, SG dome nodalization, capacity or timing of the SG relief valves)".

3 "The largest differences between the three codes RELAP5, CATHARE and SMABRE show up in the treatment of the condensation phenomena. Although large differences are observed in the condensation and vapor generation rates between different codes, the resulting pressure differences in the primary are small, while large pressure differences are found in the affected SG ".

4 "Parametric studies have shown that such code deficiencies can be compensated by a change in the nodalization. This raises the question of nodalization guide-lines for the code users, based on code assessment exercises".
6. CONCLUSIONS.

1 The building of a plant model is an important stage with many specific problems that require a collective effort. This stage should be considered in its own entity and separated from the user effects generated when using an already built model.

2 An important source of uncertainty comes from the interplay between different model sections. Although two phase problems should be considered from this perspective as an important aspect, an appropriate balance of the different sections is a key quality factor.

3 Plant calculations of transients have advantages for code assessment but also severe limitations. Detailed analysis of global transient measurements and decoupling of the TH section are required to obtain conclusions. Generalized use of data acquisition systems is very beneficial.

4 The qualification process of a plant model is essential and it is necessary to improve methodologies that guarantee the quality and prevent degrading the predictive capability of the codes.

5 Characterization of transients in the scaling groups domain helps the applicability of TH codes to actual plants through comparison with selected experiments. Codes tuned to match experiments in other scaling domains are dangerous to use.

6 The existing codes have already demonstrated, when coupled with verified models, their capability to help understanding the interaction of plant systems and TH phenomena under real transient conditions.

7. ACKNOWLEDGMENTS

We wish to acknowledge F. Pelayo, R. Mendizabal and J. Hortal, as well as the rest of the members of the Area of Technological Programs of CSN for their contribution to this paper. Also the cooperation of the ICAP-SPAIN organization and useful comments from W. Wulff (BNL) and R. Shultz (INEL) are greatly appreciated.
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SECTION 2.1.


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SECTION 2.2.


SECTION 2.3.2.


SECTION 2.3.3.


SECTION 2.3.4.


SECTION 2.4.1.


SECTION 3.2.1.


SECTION 3.2.2.


SECTION 3.3.


SECTION 4.


SECTION 4.3


SECTION 4.4.1.


SECTION 4.4.2

SECTION 4.4.3.


SECTION 4.5.


SECTION 5.1.1.1.

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SECTION 5.1.3.


SECTION 5.1.6.


SECTION 5.2.


DEVELOPMENT AND ASSESSMENT OF COMPUTER CODES FOR ACCIDENT MANAGEMENT

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ABSTRACT

Core melting at high pressure can lead to an early containment failure. Accident Management (AM) procedures have been developed to prevent such an event. Secondary and primary bleed and feed are effective measures to prevent core melt. This is demonstrated by a calculation for a total loss of feed water case with AM measures on the secondary side.

Best estimate calculations are necessary to assess the effectiveness of AM procedures. The thermal-hydraulic models in the codes have to be applicable and verified for the phenomena governing these special accidental sequences.

The computer code ATHLET is being developed by GRS as an advanced best-estimate code for the simulation of leaks and transients in PWR's and BWR's, including beyond design basis accidents. The assessment matrix for this code includes experiments relevant for the phenomena encountered in AM sequences.

Integral experiments related to AM procedures were conducted in the test facilities PKL-III, LOBI-II, BETHSY, ROSA-IV/LSTF and others. These experiments not only support planning and assessment of AM procedures but also revealed the phenomena dominating the sequence of events. The experimental results are a valuable data source for code assessment.

BETHSY Test 9.1 b was the basis for OECD/CSNI International Standard Problem No. 27. The test simulated a 2" cold leg break in a PWR without HPIS and with intentional depressurization. The calculation with the code ATHLET for ISP-27 is presented in comparison to the test results. The results show the code's capability to simulate the fluid-dynamic and heat transfer phenomena involved in this test. Conclusions are drawn with respect to modelling deficiencies and further needs for development and assessment.
1 INTRODUCTION

Large thermal-hydraulic system codes have been developed and verified in several OECD countries during the past two decades. The aim of this development has shifted during this period from conservative calculations for design basis accidents to realistic analyses of loss-of-coolant accidents (LOCA) and transients.

With the planning and implementation of accident management (AM) procedures in the existing nuclear power plants (NPP) a new field of application was opened for which the codes were not originally intended and for which assessment is not yet completed.

Although AM involves many more subjects besides thermal-hydraulics it can be clearly stated from actual practice in Germany that thermal-hydraulic codes are valuable tools to support planning and assessment of specific AM procedures. During these applications, needs for further development and assessment were identified.

2 ACCIDENT MANAGEMENT

The recent CSNI Specialist Meeting on Severe Accident Management Programme Development has shown that most OECD member countries have actively started working on severe accident management programmes /1/. In the F. R. of Germany, investigations of possible AM measures were started several years ago /2/. The meaning of the term "Accident Management" is not exactly the same in all countries. Therefore, we shall use the definition from a recent report that was jointly prepared by IPSN and GRS /3/ here.

"Accident management" includes all measures to prevent core damage and retain the core within the reactor vessel, maintain containment integrity and minimize off-site releases.

This definition includes a distinction between prevention and mitigation aspects:

(1) Prevention considers measures to avoid damage to the reactor core. Due to the relatively slow development from an initiating event to major core degradation there is in principle the possibility for the plant personnel to identify and diagnose the status of the plant and to restore safety related functions, e. g. by reactivating safety or operational or additional systems. These measures are considered to have priority over measures with mitigative character.

(2) Mitigation considers measures to control and minimize the consequences of core melt sequences. If measures to maintain sufficient core cooling and decay heat removal fail, core melt will start progressively. Even in this case measures to control and minimize the consequences can be initiated. The final goal is to avoid an uncontrolled and large release of fission products into the environment. This can be
achieved by maintaining the integrity of the containment and by limiting release rates.

This paper will be confined to some aspects of thermal-hydraulic code development and assessment for preventive AM measures. Although numerous possibilities for intervention to prevent core melting have been investigated also for Boiling Water Reactors (BWR's), the paper will mainly deal with Pressurized Water Reactors (PWR's). Concerning the computer codes, emphasis is given to ATHLET since most of the authors' experiences are based on this code.

3 PREVENTIVE ACCIDENT MANAGEMENT MEASURES

In the German Risk Study, Phase B /4/, preventive accident management measures have been analyzed and evaluated with respect to their effectiveness to prevent core melt.

The overall frequency of uncontrolled accident sequences (here: core melt) can be reduced by one order of magnitude (from \( 2.5 \times 10^{-4} \) a\(^{-1}\) to \( 2.5 \times 10^{-5} \) a\(^{-1}\)) by preventive accident management measures. It is important to mention that the more severe accident sequence, the core melt at high primary system pressure, can be reduced by nearly two orders of magnitude in frequency.

Results from analyzing plant internal accidents show that the major contributions to event sequences not coped with for design reasons stem from transients and loss-of-coolant via small leaks. In many cases, the cause of the uncontrolled sequences is the failure of steam generator feeding. Thermohydraulic analyses show that these sequences initially involve slow changes of the conditions prevailing in the reactor coolant circuit. Therefore it is still possible to prevent by accident management measures a melting of the fuel even after safety system failures.

The most effective AM measures to cope with this event are bleed and feed procedures, preferably activated for the secondary system. Apart from the measures on the secondary side, direct interventions on the primary side are also possible in order to relieve the high pressure in the reactor coolant circuit.

3.1 Calculations Performed for Preventive AM Measures

A considerable number of accident sequences were analyzed by GRS that start from different initiating events and include AM measures /5/, /6/. The most important ones for PWR's are:

- AM measures on the secondary side
- Total loss of feed water
- Station black-out

- AM measures on the primary side
  - Loss of secondary heat removal
  - Primary leaks with loss of secondary heat removal or failure of High Pressure Safety Injection (HPSI)
  - SG-tube ruptures with failure of pressurizer spraying

The analyses were performed, for a large part, to evaluate the risk reduction potential of AM-measures in the German Risk Study /4/. Detailed analyses for selected transients are being performed now to support planning and assessment of specific AM procedures.

Also for German BWR's, several leaks and transient involving AM-measures were analyzed /7/ , /8/. At present, GRS is performing a probabilistic safety study for a 1300 MWe BWR. Additional AM sequences are analyzed for this study.

The calculations were originally done with the code DRUFAN /9/. Since 1989, all calculations are performed with ATHLET, using best-estimate models (see section 4.2 below).

3.2 Calculation for Total Loss of Feed Water and Use of the Feed Water Tank

A total loss of Steam Generator (SG)-feed is assumed, i.e. loss of main feed water and failure of emergency feed water. This case has a special importance, because non-availability of feed water supply was found to be the largest contributor (almost 70%) to the frequency of accident sequences not coped with by safety systems, considering only plant internal triggering events /4/. The calculation for this transient is described below in some detail for illustration of the phenomena and the code challenges.

Fig. 1 shows the feed water and main steam system of the plant. A detailed modelling of the feed water system via the code input is required for a realistic simulation of the flow during the secondary bleed and feed measure. In Fig. 2 the geodetic proportions of the feed water system can be seen. The feed water temperature is 453 K with a saturation pressure of 1.0 MPa between feed water tank and HP-preheater. Between the preheater and the steam generators the temperature is 484 K with a saturation pressure of 2.2 MPa. Nonreturn flaps control the back flow in the system.

During the secondary bleed and feed measure a pressure profile develops along the feed water lines. In nearly stagnating flow a phase separation is possible with steam up flow
Fig. 1: Feed water and main steam system for PWR 1300

Fig. 2: Feed water system for PWR 1300
and water down flow, especially in the vertical pipe sections. Therefore, the effectiveness could be decreased by large amounts of steam in the injected feed water.

The important results of the calculation are presented in Figs. 3 and 4. The beginning of the transient and time of the total loss of feed water is 0.0 s. 40 s later, the secondary water level decrease initiates the scram of the reactor and the turbine by the reactor protection system. After closure of the steam valves the pressure on the secondary and primary side increases but will be limited by the automatic operation of the secondary relief valves. The pressure on the secondary side will be kept constant at a level of 7.5 MPa. Due to the late reactor scram the remaining secondary side water inventory is only about 52 % of the initial inventory. In this case the reactor coolant pumps will still be in operation and will provide good core cooling. However, the hydraulic energy of these pumps (24 MW) heats the primary fluid and this leads to a faster decrease of the secondary side inventory. Within 1200 s after scram the secondary side of the steam generator is completely dry and the energy transfer to the secondary side ceases.

The primary side will heat up and the pressure will be limited by the pressure control with the pressurizer spray system. At approx. 2100 s the pressurizer is full of water, the pressurizer spray nozzle gets flooded and the pressurizer relief valve opens for pressure limitation. Then main water is discharged. A further increase of the valve mass flow occurs at 3000 s, when flashing begins in the upper plenum causing a large fluid flow into the pressurizer. Then also the second relief valve opens. For this calculation it has been assumed that the valves were designed for such water flow conditions.

The core recovery starts at 4300 s, according to the large discharge flow. Secondary bleed is initiated at 60 min. After that, the pressure on the secondary side decreases in a short time to the saturation pressure of the preheated feed water of 2.2 MPa. Then a first flashing of preheated feed water and steam begins. The additional steam causes a delay of the further secondary pressure decrease, according to the limited steam valve relief capacity. Despite the flashing of steam the primary pressure is lowered. The pressurizer valves close and the primary coolant loss ends. In this case it is assumed, that the operator opens the control- and shut-off-valves only if the pressure before the valves is lower than 1.0 MPa. Therefore, the control valves are opened at 3800 s. The temperature of the feed water between the control valves and the HP-preheaters is still 484 K and the pressure is 2.2 MPa. The resultant high pressure difference causes an instantaneous high fluid flow into the steam generator and a pressure peak due to evaporation. The primary pressure is lowered under 11.0 MPa at 3000 s. The combination (ECC-criteria) of high containment pressure and low primary pressure initiates the automatic shut-off of the reactor coolant pumps and ends the addition of hydraulic energy to the primary coolant. Good core cooling was provided by the running reactor coolant pumps, even when the primary side inventory was partially depleted. After switch-off of the reactor coolant pumps phase separation starts and the water gathers in the downcomer- and core region. In this case the core is sufficiently covered with fluid and is cooled in a "reflux condenser mode".

The delay of the decrease of the secondary pressure by the flashing of feed water and steam out of the feed water line section between the control valves and the feed water pump shut-off valves continues. At 4500 s the secondary pressure falls below 1.0 MPa. At that time the shut-off valves are opened and a constant emptying of the feed water tank
Fig. 3: Total loss of feed water with secondary bleed & feed: primary and secondary pressure

Fig. 4: Total loss of feed water with secondary bleed & feed: collapsed level in RPV and steam generator
inventory into the steam generators begins, causing a nearly continuous depressurization of the primary side. The start of the injection of borated water by the HP-safety injection pumps at 11.0 MPa and by the accumulators at 2.6 MPa is not considered in this calculation, because of sufficient core cooling even without these systems. At 7000 s the primary pressure is below 1.0 MPa so that at least 2 Low Pressure (LP)-safety injection pumps can refill the primary side with borated water and then cool the primary side in a residual heat removal mode.

The remaining feed water inventory is 17% of the initial inventory when the operating range of the LP-safety injection systems is reached. In this case, a late initiation of secondary bleed and feed has been assumed. Normally an early initiation is desirable to limit or suppress coolant loss into the containment. According to the then higher primary side inventory, which has to be cooled down to the temperature and pressure to switch over to the residual heat removal system, the necessary water mass to be evaporated in the steam generators is higher. But even in these cases the feed water tank inventory is sufficient to cool down the primary side to the operating range of LP-safety injection systems.

The collapsed water level in the reactor pressure vessel is shown in Fig. 4. In the previously discussed base case the reactor control systems were activated. This is the normal operating mode. If they are not activated the beginning of core recovery is 400 s earlier. So a sufficient core cooling is only possible without the use of safety injection pumps, if the secondary bleed and feed measures are initiated ca. 10 minutes earlier or the reactor coolant pumps are shut off by the operator at least 30 minutes after the beginning of the transient. The transient level behavior shows clearly the important influence of the addition of hydraulic energy by the running reactor coolant pumps. If the secondary bleed and feed measure is initiated after 60 minutes instead of 50 minutes (reactor control system deactivated) safety injection pumps are needed to refill the core region for sufficient core cooling. The safety injection is automatically activated by the reactor protection system.

4 COMPUTER CODES FOR THERMAL-HYDRAULICS

Best-estimate system codes have been developed in several countries. In the U.S., RELAP5 and TRAC have reached maturity/10/. Quantification of uncertainties has progressed. In Europe, CATHARE and ATHLET development is continuing, and the verification process is well underway. To the authors' knowledge, however, not all problems with respect to code applications, especially for beyond design basis accidents, have yet been solved.

4.1 Challenges to the Codes for AM Applications

The rather detailed description in the previous chapter for the secondary bleed and feed case was given in order to exemplify the processes to be simulated. At first sight, the thermal-hydraulic and heat transfer processes during this type of AM transients are not
much different than those for other operational transients. There are features, however, that pose additional requirements for the codes.

- Amount of systems:
  Systems that are usually modelled by logical functions only during simpler transients, e.g. feed water system, need to be modelled by thermal-hydraulic objects. All possibly intervening control and protection systems have to be modelled completely.

- Detail of modelling:
  Thermal-hydraulic modelling requires detailed nodalization in many cases, e.g. for feed water line, surge line, cross-over legs in the loops. Due to asymmetries, several loops and several groups of SG-tubes have to be modelled.

- Range of validity:
  Candidate transients for AM are often developing into far off-normal ranges of thermal conditions. Models should have a certain extrapolation capability beyond their original data base. Generic models based on dimensionless parameters might deserve more confidence than purely empirical correlations.

- Accuracy:
  For scoping investigations and parametric studies it can be convenient and economical to apply rather simple but fast running codes based on mass and energy balances. For detailed assessment of actual plant procedures, however, accuracy of predictions is highly desirable, since the feasibility of a procedure may crucially depend on the time available to prepare and actuate AM actions.

- Uncertainty:
  Since models will not be perfectly verified, especially for off-normal conditions, quantification of uncertainty ranges for key results (water levels, times to reach setpoints) should be helpful.

- Scope of investigations:
  AM investigations, unlike design basis accidents, are not following a unique path, but have to investigate alternatives. The codes should therefore allow for flexible changes at restarts and should be operable in an interactive mode, e.g. in a plant analyzer.

4.2 The ATHLET Code

The computer program ATHLET (Analyses of Thermal-hydraulics for LEaks and Transients) is being developed by GRS for the whole spectrum of leaks and transients in PWR’s an BWR’s /11/. The code is based on a five-equation system (mixture momentum equation with drift) and is now being extended to a full six-equation two-fluid model including non-condensible gases. The reactor coolant system is modeled by a network of one-dimensional components (objects), allowing for cross flow between parallel channels. The time integration method is fully implicit.

The following features of ATHLET are of special interest for applications to small leaks and transients with AM measures.
- Steady-state capability:
  A true steady-state, i.e. time independent solution, is calculated to establish the initial conditions.

- Critical discharge model:
  The critical two-phase flow rates for a given geometry are calculated in a pre-processor step as function of the upstream conditions and stored for use during the transient run.

- Full-range drift-flux model:
  A model for the relative velocity between the two phases was developed, based on experimental data for counter-current flow limitation in various geometries /12/. The model comprises options for vertical, horizontal and inclined channels.

- Dynamic mixture level tracking:
  In a user defined vertical stack of cells, a two phase level with bubble rise below and droplet entrainment above is calculated and dynamically traced across cell boundaries.

- General Control Simulation Module (GCSM):
  A high level simulation language allows via input control to model protection and other balance-of-plant (BOP) systems. Control circuits or even simplified fluid systems are convenient to model this way. GCSM has a general interface for user provided external BOP models.

- Integrated Mass and Momentum Balance (IMMB):
  This simplified treatment of the mixture momentum equation (one dynamic pressure for a whole loop) is a fast running option especially valuable for long transients. This technique is being extended to permit local flow reversal.

- Plant Analyzer:
  The code can be run in an interactive mode in the frame of an engineering plant analyzer.

Even with the 6-eq. model being available it is planned to keep the 5-eq. model as an option in order to preserve some of the features above, e.g. mixture level tracking and drift-flux.

Assessment calculations and plant applications performed inside GRS and by external partners, have shown that model improvements and enhanced robustness of the numerics are still necessary.

A version ATHLET-CD (Core Degradation) is being developed /13/. The thermofluiddynamic module is closely coupled with modules for core melt, fission product release and fission product transport in the primary system.
5 ASSESSMENT OF CODES FOR AM-TRANSIENTS

In 1987, OECD/NEA issued a report /14/ compiled by the Task Group on the Status and Assessment of Codes for Transients and ECC. It contained proposed validation matrices for LOCA and transients, selected according to the dominating phenomena and the available test facilities. Meanwhile, the Task Group has updated the matrices and extended their work also to separate effects tests /15/.

5.1 AM Related Phenomena and Test Facilities

The matrix for PWR small breaks and transients was particularly extended by AM related tests for a non-degraded core. Among the occurring phenomena identified there, the most significant ones are:

- Natural circulation including reflux condenser mode
- Asymmetric loop behavior
- Leak flow
- Phase separation, level formation, and stratification
- ECC mixing and condensation
- Loop seal clearance
- Pressurizer and surgeline thermal-hydraulics
- Non-condensible gas effects
- Accumulator behavior

Besides the full scale PWR for natural circulation, 7 integral test facilities were found suitable for assessment of AM related procedures. The test facilities LSTF, LOBI-II, PKL-III, and BETHSY will be discussed in the following.

The other three, i.e. LOFT, Semicale, and SPES, are not discussed here further. It should be mentioned, however, that they are an active part of the assessment data base for RELAP and TRAC concerning bleed and feed /18/. The International Code Assessment and Application Program (ICAP) for RELAP5 and TRAC comprised a large number of tests from various facilities /17/.

Although this paper concentrates on PWR's, it should be mentioned that a CSNI validation matrix was set up also for BWR's. Experiments relevant for AM procedures in BWR's were conducted, e.g., in the ROSA-III facility /18/.
5.2 Integral Tests for Code Assessment

In the following, the four major integral test facilities are briefly described and their role for code assessment, especially for ATHLET, is outlined.

**LSTF**

Under the ROSA-IV program are operating two major test facilities, the Two-Phase Flow Test Facility (TPTF) for separate effects tests, and the Large Scale Test Facility (LSTF) for integral system tests.

The LSTF is a full-pressure 1/48 volumetrically-scaled full-height model of a Westinghouse-type 4-loop (3423 MW(e)) PWR. This facility has two symmetric primary loops, each one representing two out of the four PWR loops. Each loop has an active SG consisting of 141 full-size U-tubes.

Phenomena relevant for AM were investigated in numerous small-break and transient experiments. Natural circulation, horizontal flow stratification and loop-seal clearance were extensively studied/19/.

For these investigations, it is an advantage of this facility with respect to model verification that the loop diameter (0.207 m) is fairly large compared to other integral test facilities.

Many tests were dedicated to primary and secondary system depressurization/18/.

Phenomena important for the success of secondary or primary bleed and feed AM measures were investigated, e.g. redistribution of liquid in the loops during depressurization, liquid hold-up in the hot legs due to counter-current flow limitation, cyclic clearing and refilling of loop seals, mixture level swell in the core and its effect on core cooling.

The RELAP5/Mod3 code was applied by JAERI and others to LSTF tests/20/. Based on these experiences, some code models were modified, considering the results of separate effects tests.

The code changes relevant to the analysis of LSTF tests include:

1. use of the Bernoulli equation for single-phase liquid discharge through a sharp-edged orifice
2. use of the maximum-bounding critical flow model for two-phase discharge through a sharp-edged orifice
3. use of the break liquid entrainment models of Yonomoto & Tasaka, and
4. use of an interfacial drag calculation scheme in which the junction interfacial drag coefficient is equated to that of the adjacent upstream volume.
With these models changed, the agreement of calculations with test data was improved.

With the CATHARE code, several LSTF tests have been analyzed for code assessment.

LSTF tests are also part of the ATHLET assessment matrix. One of the small-break LOCA tests was chosen for CSNI-ISP-26. Seventeen participants with 8 different codes submitted post-test calculations. The ATHLET calculations received a high ranking in the final comparison report /21/.

**LOBI**

The LOBI-MOD2 test facility is a scaled (1:700) two-loop high-pressure integral system test facility designed to simulate a four-loop 1300 MW_e PWR during steady state and transient off-normal conditions.

The test program investigated small-break LOCA and transient phenomenologies with inclusion of recovery procedures and AM strategies. LOBI tests play an important role in the ATHLET assessment matrix. From the MOD2 series, the following transients with AM have been chosen for ATHLET:

- **BT-02** Loss of all feed water, primary bleed and feed
- **BT-17** Loss of all feed water, secondary bleed and feed

LOBI tests also play a role for CATHARE assessment /22/. For test BL-12, simulating a 1% cold leg break without availability of the HPIS, a blind prediction exercise was organized with the four major system codes participating /23/. It was an excellent test for code assessment, with many important phenomena observed including global mixture level formations and stratification, core dryout and thermal non-equilibrium effects from cold accumulator injection. Some codes had difficulty in predicting system behavior after dryout occurred. In this test, the primary pressure response was an important factor in controlling mixture level variations which influenced break flow rate, core uncoverly, decoupling of primary and secondary sides, time of accumulator injection, and loop seal clearing. To model these aspects reliably, the codes need to correctly predict break flow as a first priority, together with vapor generation and condensation rates in the vessel riser and downcomer regions.

**PKL**

PKL-III is a scaled (1:145) integral system test facility simulating a 1300 MW_e Siemens PWR /24/. The facility has four loops with active pumps. The four steam generators are equipped with 30 U-tubes each of original size. Primary pressure is limited to 4 MPa. Core power reaches 10% of nominal rating. With respect to AM relevant tests it is important to note that the facility is not only equipped with the engineered safety systems including ECC-systems, volume control and pressure control systems, but also with all secondary side systems relevant for correct simulations of the heat sink, including feed water system and main steam lines.

The PKL-III test program consists of three series /25/:
- III-A comprised 21 experiments. After characterization tests of the facility under special 2-phase conditions, investigations were concentrated on shut-down procedures under off-normal conditions.

- III-B comprised 25 experiments. Complex transients were investigated including AM measures.

- III-C is starting now. AM procedures for small breaks and SG tube breaks will be investigated. System effects for passive safety systems will be explored.

PKL tests are of high importance for ATHLET assessment, since the tests are representative for transients and procedures valid for German PWR's. The following PKL-III tests have been included in the ATHLET assessment matrix, with possible future extensions:

- **A 4.3** Shut-down after SG tube break with 2 main coolant pumps running
- **B 1.2** Loss of feed water, secondary bleed and feed
- **B 2.1** Emergency power case, shut-down procedure with 3 SG's isolated
- **B 3.1** Shut-down procedure by natural circulation with 1 SG isolated

**BETHSY**

Designed and constructed from 1983 to 1985, BETHSY test facility has started the investigation of PWR accident situations during 1987 with the two main objectives to contribute to verify the advanced computer code CATHARE and to validate the physical bases of events and states oriented emergency operating procedures.

BETHSY is designed to model a 3 loop-2775 MW, Framatome PWR. The overall scaling factor is 1:100 while the elevations are 1:1 and the design pressures are 17.2 MPa and 8 MPa for the primary and the secondary coolant systems. Furthermore, BETHSY is equipped with all circuits and systems which can play a role during an accident or a transient /26/.

With ATHLET, two tests were calculated so far:

- **3.4a** Shut-down by natural convection. SG isolated (similar to PKL-III B3.1)
- **9.1b** (ISP-27, presented in detail below)

More BETHSY tests will be included in the ATHLET assessment matrix.
5.3 Blind calculation of the OECD International Standard Problem 27

ISP-27 is a blind problem, which is based on the BETHSY Test 9.1b, performed at the Nuclear Research Centre in Grenoble (France) on December 13, 1989. A total number of 28 blind calculations participated in the ISP-27. As a whole, 23 organisations from 19 countries have sent a submission for this ISP, and 9 different computer codes have been used. GRS participated with an ATHLET blind pre-test calculation.

Short description of the BETHSY Test 9.1b

The BETHSY Test 9.1b /27/ belongs to the multiple failure transient category, and is involved in accident management studies: It consists in a 2" cold leg break, while high pressure safety injection system (HPIS) is assumed unavailable. This transient leads to a large core uncover and fuel heatup. In this test the start of the ultimate procedure, the opening of 3 steam generator steam dumps to atmosphere, is delayed, when the maximum heater rod cladding temperature reaches 723 K. This action allows the primary coolant circuit to depressurize to the accumulator injection threshold and to the LPIS actuating pressure. The end of the test is reached, when the conditions for the actuating of the residual heat removal system are obtained.

Simulation Model Used for the ATHLET / FLUT Calculation

The analysis of the BETHSY Test 9.1b was performed with the computer codes ATHLET Mod 1 Cycle E and FLUT /30/. The object structure used to define the ATHLET/FLUT input deck for the blind ISP 27 calculation is shown on Fig. 5. The model simulates all the specific characteristics of the BETHSY facility and is arranged into 82 thermo-fluid objects with 304 control volumes and 226 junctions and 83 heat conductor objects with 206 heat slabs.

The model consists of a pressure vessel and a three loop representation of the BETHSY facility, in which each loop is connected to a separate steam generator. The pressurizer is connected to the hot leg of the broken loop 1. The accumulator tanks as well as the low pressure injection systems are connected only to the cold legs of the intact loops 2 and 3. The three loop representation of the BETHSY facility is necessary due to the asymmetrical loop seal clearing phenomena. The core region is simulated by only one hydraulic channel neglecting radial distribution of heater rod temperatures during the reflux-condensor mode core cooling.

Critical points for the modelling of the BETHSY facility are the upper part of the downcomer and the break configuration. A trefoil shaped device at the top of the external downcomer and at the connection point of the three cold legs should avoid too large flow by-passes between the cold legs. The simulation model considers this device, by connecting the cold legs to the downcomer objects below and above the cold legs. The break is simulated by a discharge valve component. The vapor and liquid pull through was determined, if the collapsed level in the broken cold leg was in the range of 2 cm above and below the center line of the break. The break flow data were calibrated at measured Super MOBY-DICK data, which were provided to the ISP participants /28/.
Fig. 5: BETHSY Test Facility: object scheme for ATHLET

Fig. 6: BETHSY Test 9.1b: break mass flow
The recirculation rate in the steam generators was set to 20 to minimize the correction of flow loss coefficients. The pressure in each steam generator is controlled by a fill component. All three steam generators are connected by a collector. The steam flow via the steam dump is determined by a pressure dependent mass flow table. The asymmetric loop seal clearance in loop 2 should be induced by a smaller necessary pressure difference for loop seal clearance. The measures are: 1 cm higher cross-over leg, 3 kg/s lower circulation flow and 0.02 MPa higher secondary control pressure. This simulation was not successful, because in loop 2 the natural circulation broke down earlier due to the measures mentioned above. The fluid in the loop seal cooled down faster due to the pump heat losses and increased the necessary pressure difference for loop seal clearance in loop 2 so far, that loop seal clearance occurred in the broken loop 1.

Almost all steady state conditions were calculated inside the error band of the measurements. Only the secondary mass inventories are about 5 % lower as specified for the test.

### Table 1: Sequence of Events for BETHSY Test 9.1b

<table>
<thead>
<tr>
<th>Event</th>
<th>Time (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Test</td>
</tr>
<tr>
<td>Transient initiation</td>
<td>0</td>
</tr>
<tr>
<td>Scram signal</td>
<td>41</td>
</tr>
<tr>
<td>Pressurizer empty</td>
<td>50</td>
</tr>
<tr>
<td>SI-Signal</td>
<td>54</td>
</tr>
<tr>
<td>- closure of turbine valve</td>
<td></td>
</tr>
<tr>
<td>- shutdown of feedwater pump</td>
<td></td>
</tr>
<tr>
<td>Auxiliary feedwater on</td>
<td>82</td>
</tr>
<tr>
<td>start of pump cooldown</td>
<td>356</td>
</tr>
<tr>
<td>end of pump cooldown</td>
<td>971</td>
</tr>
<tr>
<td>Break uncover</td>
<td>1350</td>
</tr>
<tr>
<td>Start of the first core depletion</td>
<td>1830</td>
</tr>
<tr>
<td>First loop seal clearing</td>
<td>1944</td>
</tr>
<tr>
<td>Start of seconds core uncover</td>
<td>2180</td>
</tr>
<tr>
<td>Ultimate procedure initiation</td>
<td>2562</td>
</tr>
<tr>
<td>Accumulator injection</td>
<td>2961</td>
</tr>
<tr>
<td>Second loop seal clearing</td>
<td>3067</td>
</tr>
<tr>
<td>Maximum core temperature</td>
<td>3040</td>
</tr>
<tr>
<td>Total core quenched</td>
<td>3450</td>
</tr>
<tr>
<td>Accumulator isolation</td>
<td>3831</td>
</tr>
<tr>
<td>L.P.S.I initiation</td>
<td>5177</td>
</tr>
<tr>
<td>End test/calculation</td>
<td>10000</td>
</tr>
</tbody>
</table>
Results of the blind calculation

Table 1 provides the measured and calculated sequence of events for the BETHSY Test 9.1b. The Figures 6 to 10 show the principle parameters (pressure, break mass flow, pressure differences and core temperatures) involved in the test. The test is initiated by the opening of the break valve in the cold leg 1. Due to the high mass losses during the subcooled blowdown (Fig. 6) a fast depressurization (Fig. 7) takes place on the primary side at the beginning of the transient. The scram signal occurs at 36 s (test: 41) because the primary pressure falls below 13.1 MPa. Shortly later at 55 s (test: 54 s) the safety injection (SI) signal is actuated at the primary pressure of 11.9 MPa. However the complete unavailability of the high pressure safety injection (HPSl) system is assumed. Shortly before at 54 s the pressurizer is empty. After 85 s saturation conditions are reached in the hot legs and the primary pressure (Fig. 7) is stabilized. Together with the SI signal, the shut-down of the feed water pumps and the closure of the turbine valve is initiated. Therefore the steam generator pressure (Fig. 8) increases from 6.88 MPa to 7.03 MPa, the control pressure of the relief valves. The auxiliary feed water pumps start at 84 s (test: 82 s) to refill the steam generator up to the level of 13.72 m. After 550 s the condensation capacity of the auxiliary feed water exceeds the heat flow rate from the primary to the secondary side. The pressure control valves at the steam generators are closed up to 1050 s and the secondary pressure falls in this time interval below 7.03 MPa. Due to the lower secondary temperature also the primary side is cooled down in this time period and also the primary pressure shortly decreases.

Between 354 s and 972 s (test: 356 s to 971 s) the main coolant pumps coast down. Due to the continuous mass depletion during the saturated blowdown (Fig. 6) the natural circulation breaks down at 800 s in loop 2, at 820 in loop 3 and 917 s in loop 1. Afterwards the core is cooled by the reflux condenser mode.

The break is uncovered (Fig. 7) at 1500 s (test: 1350 s) The break flow quickly changes from saturated water to saturated steam.

The first core depletion (Fig. 9) and core heatup (Fig. 10) start at 2000 s (test: 1830 s). At 2073 s (test: 1944 s) the first loop seal clearing occurs incomplete in break loop 1 in the calculation instead of loop 2 in the experiment. This leads to a short increase of the water level in the core and to an interruption of the core heat up. The second core uncovery starts at 2096 s (test: 2180 s). The actuation signal for the ultimate procedure, the maximum core temperature of 723 K (Fig. 10) is reached at 2440 s (test: 2562 s) The steam generator dump valves are opened to depressurize the secondary and primary system down to the accumulator pressure and the maximum pressure head of the LPSI pumps. The fast pressure decrease on the primary side causes a short increase of the water level in the core and a short stagnation in the core heat up.

At 2865 s (test: 2961 s) the primary pressure falls below the accumulator pressure and the accumulator begins to inject. Shortly later the second loop seal clearance takes place at 2917 s (test: 3067 s). In the calculation all three loop seals are cleared, instead of one loop seal clearing (loop 2) in the experiment. The loop seal clearing initiates the refill of the core. The maximum core temperature (Fig. 10) of 1061 K is reached at 2920 s (test: 3040s). The calculated maximum core temperature overestimates the measured one by
Fig. 7: BETHSY Test 9.1b; pressure in pressurizer

Fig. 8: BETHSY Test 9.1b; pressure in SG (loop 3)
Fig. 9: BETHSY Test 9.1b: water level in core

Fig. 10: BETHSY Test 9.1b: maximum heater rod temperature
about 70 K. At 3510 s (test: 3450 s) the total core is quenched. The accumulators are isolated at 3741 s (test: 3631 s) before the accumulator pressure falls below 1.5 MPa and nitrogen discharge starts. The depressurization of the accumulators is isothermal.

The maximum pressure head of the LPSI system of 0.91 MPa (Fig. 3) is reached at 5405 s (test: 5177 s). The refill of the whole primary system leads to large numerical oscillations with very small time steps. Therefore the calculation with ATHLET was stopped and the calculation was continued with FLUT /30/. The discharge flow rate is calculated in FLUT with the isentropic homogeneous equilibrium model, which strongly underestimates the break flow for subcooled upstream conditions. In addition the used injection characteristics for LPSI overestimates the injection rates. Therefore the conditions for the residual heat removal system, pressure below 2.5 MPa, core outlet temperature below 450 K and core outlet subcooling larger 20 K is reached too early at 6720 s (test: 8330 s). The analysis ends at 7150 s.

Discussion of the analysis results

The comparison between the calculated and measured time points for the main events (Table 1) shows, that all basic phenomena are predicted well. The same is indicated by the time history plots (Figs. 6 to 10). These results show the capability of ATHLET to simulate the fluid-dynamic and heat transfer phenomena involved in this test. There are still some problems, which should be solved. A reasonable break flow rate can only be predicted, if the discharge model is calibrated at provided data for the used break configuration.

The loop seal clearing is determined incomplete and in the wrong loop. The reason why it occurs in loop 2 is not yet understood. An improved modelling of the loop seal and the bypass from the downcomer to the upper head is necessary.

5.4 Observations from Assessment Calculations

From recent assessment calculations with ATHLET we learned that experiments involving AM procedures require special attention from both the experimenter and the code user in order to make the test fully profitable for code assessment.

- The initial conditions have to be exactly known and perfectly established in the code calculation. In a recent paper for the OECD about user influences on code results /31/ the importance of correct starting conditions was pointed out especially for long transients. The ATHLET code with its steady-state initializing capability has a potential advantage there. If the initial conditions of the test, however, are not fully steady-state even with such a tool it becomes difficult to establish the true conditions.

- In some experiments initial temperature differences between primary and secondary side were so small that the direction of heat flux was opposite in the upper and lower part of the steam generator, since saturation temperature on the secondary side varied with elevation according to hydrostatic head. This is a situation not foreseen in the automatic initialization procedure of the code.
6 CONCLUSIONS

Preventive AM measures are effective means to reduce core melt frequency. Secondary and primary bleed and feed are most effective measures to cope with the total loss of feed water in PWR's. Also for BWR's numerous possibilities for intervention exist.

Best-estimate thermal-hydraulic calculations with comprehensive system modelling are required for planning and assessment of AM procedures.

The data base for model qualification and code assessment consists of a considerable number of integral system experiments and numerous separate effects tests. Experimental investigations continue in several facilities.

Comparisons of code calculations with integral system experiments, especially in the frame of OECD International Standard Problems, have demonstrated the codes' capabilities to make reasonable predictions.

Deficiencies were identified in modelling specific processes, e.g. phase separation, break flow, and condensation, with the desired accuracy. Quantification of uncertainty is surely a remaining task.

Practical features, like automatic input diagnostics, steady-state option, easy modelling of balance-of-plant systems, should be realized for user convenience.

For some codes, robustness of the numerical method and speed of calculations need improvement.

ACKNOWLEDGEMENT

Development and verification of the ATHLET code is sponsored by the German Federal Ministry for Research and Technology (BMFT).

The authors are grateful to Dr. Y. Kukita of the Japanese Atomic Research Institute (JAERI) for providing information on ROSA-III and ROSA-IV experimental programs.
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SESSION 5

SPECIAL AND PROSPECTIVE AREAS

Session Chairman:
E. Hicken, Professor, Jülich
The session included five presentations, three covering special applications and two surveying thermal-hydraulics during PWR Severe Accidents and relevant thermal-hydraulics aspects of new generation LWRs.

The analysis of possible boron dilution in PWRs and related reactivity transients showed that existing - and also very sophisticated - codes have to be improved to simulate these sequences because 3-dimensional behaviour and mixing processes become dominant. If these reactivity transients threaten fuel element integrity, provisions have to be made.

Thermalhydraulics in CANDU reactors are for the most part similar to those in LWRs. However, some processes are different and very specific modelling is necessary. It has to be recognized that no code used for LWR simulation has been modified for use for CANDU reactors. For sequences with a degraded core, work by AECL is under discussion.

The thermalhydraulic system behaviour of VVER reactors is of increasing interest. In particular their steam generator behaviour deviates remarkably from the system behaviour of Western type steam generators. A need for related large-scale separate effect tests seem to be mandatory.

Proposals for new generation LWRs include more passive safety systems as well as more natural convection processes. Very often the driving forces are small due to gravity forces only. This results in 3-dimensional flows, low pressures, oscillations and steep gradients. It has also to be recognized that coupling between the coolant system behaviour and containment behaviour for the new generation reactors is stronger compared with existing reactors. It has been assessed that specific separate effect and integral tests are mandatory as well as related code improvements and validations. In addition, the numerical solution methods have to be improved to cope with the long duration sequences.

Thermalhydraulics during sequences with a degraded core is strongly influenced by exothermic chemical reactions and changes in the geometry. In addition, thermalhydraulics is coupled with other processes, high core degradation, chemical reactions and fission product behaviour. These additional effects do require additional modelling; the experimental basis is assessed as unsatisfactory. It has also been assessed that the global system behaviour at least should be simulated accurately to allow an operator to take responsible action in case of degraded core situations.

For the evaluation of experiments, more detailed modelling is necessary. It is commonly agreed that only best-estimate methods should be used. It has been agreed too that sequences with a degraded core cannot be simulated with the same accuracy as sequences with operational transients and LOCAs.

A final discussion resulted in the common agreement that work on "classical" thermalhydraulics as well as work on severe accidents is needed. This is due to the additional requirements of new generation LWRs and the Eastern European reactors.
The assessment procedure developed for "classical" thermal hydraulics could also be applied to severe accidents. There is an urgent need for better numerical methods also taking into account the upcoming generation of computers.
SYSTEM THERMALHYDRAULICS DURING PWR SEVERE ACCIDENTS

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ABSTRACT

The final objective of all severe accident evaluations is to obtain the magnitude of the potential Fission Product (F.P.) release in the environment. Consequently, severe accident experts have focused, very often mainly, and sometimes exclusively, on F.P. physical and chemical behaviour. In many evaluations, the thermal hydraulic transient was calculated by very simplified models. But the situation is evolving. By carrying out more frequent physical analyses to improve plant evaluation or to prepare experiments (such as Phrebus FP), a growing number of thermalhydraulics problems have been raised and recognized as being significant for F.P. prediction.

The paper therefore reviews the main thermalhydraulic phenomena which have to be taken into account especially examining their influence on severe accident initiation and scenario, core degradation, vessel failure and more generally RCS failure, F.P. release, transport, deposition, revaporization. The needs for model improvement and for specific assessment are listed. They cover not only the description of special thermalhydraulic phenomena, but also the handling of complex coupling with other physical modules. Prospective research directions are proposed to answer these needs.

1. INTRODUCTION

Severe accidents (S.A.) start in PWRs from physical states very similar to those encountered in design accidents. A loss of coolant or a loss of heat sink (for example loss of feedwater in steam generators or loss of the residual heat removal system) may provoke a two-phase flow inside the primary circuit, degraded core cooling with core uncovering, and the beginning of core heat-up. This transient enters the category of S.A. (severe accident initiation) when a failure of a reactor safety system inhibits core cooling recovery (core boil-off)1/. Severe accidents are therefore in complete continuity with design accidents and the prediction of their occurrence requires the same understanding.

When, during core heat-up, the temperatures of the rods reach about 1000°C, oxidation of the cladding becomes significant. This oxidation increases with temperature and produces heat which should be evacuated by the coolant. The prediction of heat removal capacity is therefore crucial as it determines the feedback between the temperature increase and cladding oxidation.

If no action is taken to recover heat removal from the core, the temperature and core oxidation continue to increase (for oxidation unless sufficient steam is supplied to the core). Interaction between materials appears as the temperature increases and may lead to liquid phases. Around 1800°C, the remaining unoxidized part of the claddings starts to melt. Core melt progresses: liquid Zircaloy dissolves some uranium oxide thus increasing the amount of liquid phase, interaction with inconel grids occurs and if the temperature increase is not stopped Zirconia and uranium oxide melting points are reached.

After the beginning of melting, relocation of materials inside the core becomes significant. Two mechanisms are involved: first, the downward flow of molten material which freezes at lower levels can then melt and freeze again, and can finally constitute a molten pool held in position by a crust formed of solidified relocated materials; second, the fall of solid
debris consecutive to embrittlement of cladding by oxidation.

If no action is taken, melting spreads and relocation progresses into the lower plenum. Depending on the thermal-hydraulic state of the lower plenum, molten material may fall into a variable quantity of water. Interaction between the corium and water occurs which may lead to steam explosions. Depending on the amount of fallen corium, the corium may freeze temporarily inside the water before melting again. In other cases, molten material interacts immediately with the reactor pressure vessel.

If action is still not taken, the interaction between the molten corium and lower plenum walls provokes vessel failure, first by thermal effects but possibly also by mechanical effects. After vessel failure, the corium falls into the containment sump where it again experiences interaction with water (with or without steam explosion) and where it starts interaction with the concrete basemat.

During the whole degradation process described above and insofar as claddings is broken, there is emission of Fission Products (F.Ps). First F.P. gases escape from the gap at the cladding rupture. Then F.Ps diffuse from the uranium oxide and are emitted as gases. This latter emission depends very much on the temperature history (values of temperatures reached but also the time period during which they are maintained) in every point of the core.

The emitted fission products are then transported throughout the circuit. During their transport, F.P.s encounter colder zones downstream from the core and condense by forming aerosols. It is the behaviour of these mixtures of gases and aerosols which will govern transport, deposition, revaporation of F.P.s and finally the amount of F.P.s which can be released to the environment. The related phenomena are mostly conditioned by the thermal-hydraulic conditions and especially by the bulk temperatures, wall temperatures, temperature gradients, velocities, turbulence, heat source and heat sinks, bulk and wall condensation. Finally the chemical forms of these F.P.s are given by the chemical reactions which take place in the aerosols, in the gases or between aerosols, gases and walls. These reactions depend in the same way on the thermodynamic states.

From this brief list of phenomena occurring during severe accidents, one can make the following observations:

- A great deal of physical phenomena occurring in a severe accident are specific to this type of accident: these are core degradation with core oxidation, core melt and relocation; F.P. emission from the fuel pellet, aerosol formation / transport / deposition / revaporation; all chemical reactions determining the final chemical species.
- When severe accident studies started, many of these specific chemical and physical domains needed considerable efforts in order to be understood and to become predictable. This explains why severe accident experts have focused mainly and sometimes exclusively first on F.P. physical and chemical behaviour and second on core degradation mechanisms (sometimes with a lower priority). Thermalhydraulics was then often considered of secondary importance.
- But a brief analysis of the phenomena listed above shows very clearly that severe accidents are characterized by strong interactions between different physical domains. No specific category of phenomena can be ignored without a risk of completely distorting the final result.

- This explains why thermalhydraulics, described very roughly in the first severe accident codes (level of modelling corresponding to the beginning of the 70's), has come to be predicted by means of the most advanced Best Estimate Thermalhydraulic Codes (RELAP5 or CATHARE for example). Moreover, the preparation of experiments or attempts to improve plant evaluation have shown that even in the S.A. area, improvements were required for these advanced thermalhydraulics codes.
Areas such as core degradation, physical and chemical behaviour of F.P.s are obviously of major importance in severe accidents. But it is also evident that thermalhydraulics conditions the overall accident. The objective of this paper is therefore to review all these thermalhydraulic aspects of S.A.s, to see whether they are correctly handled and where there are unsatisfied needs, if any. In this paper we will focus mainly on the in-vessel part of the accident. For each of the phases described above, we will first list the thermalhydraulic phenomena which have to be taken into account in S.A. analysis. Then, the available physical models and codes will be reviewed and the needs for model improvement and assessment discussed. Finally some future research directions will be examined.

2. SYSTEM THERMALHYDRAULICS DURING ACCIDENT INITIATION

2.1. Phenomena to be described

Sequences which may lead to severe accidents are of all types: they can be large, medium, or small break LOCAs as well as special transients. Often the initiating event which leads to a severe accident situation does not change the character of the physical phenomena. A large break with no safety injection for example, will terminate in a severe accident but the phenomena which have to be predicted in the initiation phase are nothing else than the usual large break phenomena encountered in design basis accident. A similar statement can be made for several special transient cases. In all these cases, the phenomena to be described in the initiation phase are not specific to severe accidents and have to be treated in the same way as the prediction of a design basis accidents.

However, severe accidents in many cases are induced by the failure of auxiliary systems which lead to a very slow evolution of the reactor coolant system (for example loss of PTR). The time constant of such transients are hours or even days. The physical phenomena then show specific features which can be analysed by examining the governing parameters of such transients, i.e., the circuit mass inventory and energy transport.

The mass inventory results from the balance between the injection flowrates and the break flowrates:
- For the injection, the easily predictable flowrates from the safety injection pumps are most often zero; consequently the only injection to be predicted is that from the accumulators. The very slow character of the transient and particularly of the primary pressure therefore gives a very pulsatory behaviour of the accumulators which requires an accurate prediction of the interaction between the injected water and the primary circuit fluid, and accurate handling of the injection valves.
- For the break, the flowrates depends very much on upstream conditions and especially on the vapour content in the flow. These conditions are a function of mass distribution along the circuit.

Mass inventory and distribution as well as energy transport result from the flow pattern in the circuit (generally natural or natural-like circulation). These flow patterns very often show oscillatory behaviour (time periods of some seconds to minutes). Stratification in the circuit may produce water plugs which maintain oscillations and which may disturb and determine the different flow paths, especially those going to the break. In these initiation phases non-condensible gases may have been injected into the circuit (for example nitrogen from the accumulator) and can significantly disturb energy transport from the core by particularly affecting condensation phenomena.
The safety issues for this initiation phase are obviously event timing and especially the beginning of the core recovery. Timing conditions the capabilities of initiating accident management procedures which could prevent the occurrence of a severe accident and as the physical phenomena which determine it may indicate, it can show quite large dispersion.

2.2. Available physical models and codes: needs for improvement and assessment

When reviewing the physical models and codes used to predict this initiation phase, a distinction has to be made between the two categories of codes resulting from the two-tier approach to severe accidents /2/.

The first approach is defined by the use of integrated codes and is based on simplified modelling. The simplification is both topological and physical. Topologically, the circuit is described by few volumes and in extreme cases by only one. Physically the two phase flow models are generally thermal equilibrium models with stratification models of integral type (RELAP4 type).

The second is the detailed mechanistic approach. Thermalhydraulics is described by the best estimate advanced thermalhydraulic codes (RELAP5 or CATHARE type). Detailed axial noding can be used to describe the topological details and all the capabilities of two fluid modelling for predicting two-phase flows are put to use in order to gain by them.

The limits of the first simplified approach are inherent to the global feature of the codes: the basic principle of these codes is to calculate the average parameters of the circuit directly using the global balance derived from the basic principles of physics. These calculations give undeniable results, but their limitation arises from the fact that they require predictions of sinks and sources of mass and energy which, especially for the sinks, are strongly dependent on the local parameters in their vicinity. The simplified approach is unable to predict the necessary detailed parameter distribution which gives these local values. In our case this is particularly important for a break flow when the upstream conditions "hesitate" between liquid or steam conditions (conditions occurring significantly in this initiation phase when the circuit is not empty enough to obtain only steam conditions as will be the case in the next phases).

The detailed mechanistic approach is therefore, in principle, in a much better position to accurately describe this phase. However there remain difficulties and this raises needs for improvement and assessment. Basically these difficulties are the usual ones encountered in LOCA and transient prediction. Some specificities, however, of the physical modelling and numerical aspects must be emphasized.

As regards the physical modelling, the break flowrate evaluation is faced with the usual difficulties. In a design basis accident these difficulties are somewhat attenuated by compensating the break flowrate uncertainties by parametric sensitivities of the break size which is considered as an unknown parameter. In S.A. initiation, compensation does not work as the break is generally assumed to be on very well known piping connected to the primary circuit. However the resulting uncertainty can in most cases be bracketed. The problems raised by the description of the oscillatory behaviour and of the non-condensable gas effects are, however, more difficult. In most codes, oscillation prediction is a weak point and non-condensable gas modelling is generally one of the more recent models introduced into the codes. For both aspects, there is either very little assessment, or none at all, and it is clear that considerable progress must be made if the present resulting uncertainties are to be reduced.
As regards the numerics, the long duration of the transient together with the oscillatory behaviour, are possible sources of numerical difficulties. The first kind of difficulties concerns the non conservation (mass and energy) problems: certain numeric defects on mass or energy conservation which remain negligible for shorter transients, become unacceptable in very long term transients due to error accumulation. Very accurate calculations must therefore be made in order to reduce these errors. This is particularly difficult in the case of oscillatory flows, but it is also essential because oscillations contribute significantly to the loss of mass at the break, and thus to the overall mass inventory. The second kind of difficulty is over-long computing time. Transients which last days require codes that run much faster than in real time. This could be achieved by using simplified topologies but this is in contradiction with the need for detailed parameter distributions (for example spatial mass). This could also be achieved by special time step management in releasing the convergence criteria, but this too is in contradiction with the necessity for accurate transient description (mass and energy conservation). The simplified approach of some integrated codes do not answer the question either, as they are almost the same as the extremely simplified topologies of the mechanistic codes. Considerable effort should therefore be made in order to obtain more accurate and faster codes.

3. SYSTEM THERMALHYDRAULICS DURING CORE BOIL OFF, CORE HEAT UP AND CORE OXIDATION

3.1. Phenomena to be described

In this next phase, after the onset of core uncovering, the state of the primary circuit is characterized by a low mass inventory. Core boil-off is followed reversibly, if no action is taken, by core heat-up and consequently by extensive cladding oxidation. The total mass inventory and also mass distribution greatly affect transient evolution. The smaller the total mass inventory, the larger the influence of mass distribution on core behaviour is. A description of liquid capture in parts of the circuit is therefore crucial. Such captures have been observed in experiments, for example in steam generators or in pressurizers. Energy transport is the second kind of phenomenon of special importance. It includes heat generation and removal inside the core. Heat generation is not only due to residual power, but also to oxidation reaction latent heat. Two parameters significantly govern oxidation: wall temperature and steam starvation. Oxidation is therefore strongly dependent on the flows in the core. For these flows, large recirculation loops /3/ may develop in the vessel which, together with radial and axial heterogeneities in the core, induce complex 3-D core flow configurations and consequently large spatial oxidation differences. This also affects heat transport from the core by heating all the vessel structures of the upper plenum and upper head. Besides this 3-D behaviour in the vessel, energy transport is ensured by natural circulation in the loops. Here again complex multidimensional behaviour is expected and has been demonstrated in some experiments /4/. Circulation loops may develop in the hot legs with hot gas going from the vessel to the steam generator and colder gas going back from the steam generator to the vessel. In the steam generators, gas circulation from the hot plenum to the cold plenum is obtained in some steam generator U-tubes, while reverse flow is established in the others. Natural circulation is, as in the preceding phase, influenced by water plugs resulting from stratification in the lower parts of the circuits, by oscillations, and by the effect of non-condensible gases. The latter's influence is certainly quite significant, firstly because their quantity is increased by hydrogen production in the core, and secondly because their effect on any condensation which may occur in the steam generators is known to be considerable.
Safety issues are numerous:
- The kinetics of mass depletion in the core and energy transport strongly influence elapsed time before the onset of core melting.
- Temperature transients and 3-D temperature maps are directly connected to oxidation and corollatively to hydrogen production. They also govern fission product release from the fuel. Both hydrogen and F.P. release are obviously very important safety issues /5/.
- RCS temperatures, resulting from energy transport from the core, determine RCS capability to sustain mechanical loads. In the case of a pressurized transient, this could induce mechanical failures, for example of surge lines or in a potentially more severe case, of steam generator U-tubes leading to the possibility of containment by-pass /6/.
- RCS temperatures significantly determine F.P. retention, as will be discussed later.
- Operator actions should be defined during this accident phase in order to avoid consequences which could aggravate remaining mass and energy transport.

3.2. Available physical models and codes: needs for improvements and assessment

The simplified approach of most integrated codes makes it difficult to describe this phase accurately (for example difficulties in describing mass distribution and energy transport) mainly because its coarse topological description, and because the codes do not generally contain two-phase flow non-equilibrium modelling. Code description, at least during some part of the transients, may also not be conservative. In the event, for example, of water being captured in steam generator U-tubes, simplified models take this water into account in the core. In the simplified models, the water will then participate in core cooling and will consequently delay oxidation and hydrogen production. Whereas with detailed codes, due to a more realistic core liquid inventory, oxidation will take place earlier. Moreover, if the thermal-hydraulic transient allows, much later, the water captured in the steam generator to reach the core, it could participate in feeding oxidation in a high temperature steam starved core, instead of having cooled the core as in the simplified model.

Advanced thermal-hydraulic codes clearly have better capabilities. Their 1-D model may describe overall transients in all the piping. However it is known that they have weak points which need better modelling and mostly better assessment, especially in counter current flow limitation modelling (essential for the prediction of liquid capture in some parts of the circuit). In this domain experiments are underway in order to improve the situation which is also encountered in conventional breaks or transients. Description of the 3-D behaviour of the core and of the asymmetric operation of steam generator U-tubes, are also part of the potentialities of these advanced codes. However not all the codes have transformed these potentialities in capabilities. For 3D, only TRAC code has full 3-D capability, /7/ CATHARE will obtain it in its next version. For the asymmetric operation of steam generator U-tubes, some attempts in experiment prediction are not fully convincing and require at least more assessment. The problems raised by the modelling of non-condensable gases in the codes have been discussed previously. The same statements apply here, but with more vigour because of their effect on heat transfer by condensation. More generally all heat transfer modelling in the codes should be completed so as to take into account high temperature effects such as radiative heat, and finally should also be better assessed.

4. SYSTEM THERMALHYDRAULICS DURING CORE MELT AND CORE RELOCATION

4.1. Phenomena to be described

When significant core melt begins to occur, only a small mass of liquid water remains in the lower part of the core and in the lower plenum. Core flow patterns are of the same
kind as in the preceding phase except that core slumping, core freezing, remelting and refreezing changes the boundary conditions of the flow. Partial or complete flow blockages induce flow redistribution. In the case of pressurized transients, these redistributions interact in particular with the 3-D recirculation in the vessel. More generally, it has been shown by reactor core degradation calculations with the ICARE code [8] that the core slumping scenario, crust and molten pool formation are very sensitive to flow redistribution following blockage formation. Thermal hydraulics and core degradation thus appear to be entirely coupled. An accurate multidimensional description of the core flow is therefore necessary in order to be able to apply the core degradation models in realistic conditions. This is particularly crucial for solving the scaling problem. Indeed all degradation codes are assessed on experiments with a limited number of rods, which, [9] even if there are radial heterogeneities, can be considered as regards thermal hydraulics as being almost 1-D. Extrapolation to the reactor case where the core diameter is nearly equal to the core length therefore means considering the local behaviour (at the scale of one or a few assemblies) and to place these local meshes side by side in an environment which is necessarily multidimensional thermal hydraulics.

At this stage of the list of phenomena to be described, the question of the core reflooding capabilities must also be mentioned. The need to predict what occurs in the case of water injection in the core due to some event or to operator action, is in fact a constant one throughout the transient. At an early stage this will be similar to core reflood in a LOCA scenario. When core degradation progresses, core reflood becomes more and more difficult to describe with the highest complexity when the core relocates [10]. At this time, effects of blockages must be taken into account as well as the presence of molten material. Complex 3-D two-phase flows and heat transfers take place which have to be evaluated in order to obtain the thermal and mechanical behaviour of the degraded core. The risk of fuel coolant interaction should also be assessed. During such reflood the flows in the RCS are certainly highly perturbed and should be accurately described because of their possible action on F.P. resuspension and on the transport of the hydrogen peak provoked by the injection of water in the core [11].

Safety issues during this phase specifically concern F.P. release and to a lesser degree hydrogen production. It is clear also that core coolability and the definition of appropriate procedures for operators are very important safety issues. But this phase is probably even more important in view of its consequences on the next phase, i.e., lower head failure. Indeed it defines the way the core may relocate in the lower head (location and amount of burn rate, chemical state especially degree of oxidation) and this will entirely determine the lower head failure scenario (see next chapter).

4.2. Available physical models and codes: needs for improvement and assessment

As it can be seen, the phenomena to be described are very complex. Even for mechanistic codes this phase is very difficult to describe. In these codes all the mechanisms could be potentially predicted, but:

- multidimensional flow redistribution should be introduced into the codes or at least assessed if it is programmed;
- the assessment of heat transfers in a degraded geometry such as this, should be made;
- coupling between degradation and thermal hydraulics should be improved from the physical and numerical aspects;
- almost everything remains to be done for the modelling of molten core reflooding.

Large areas of improvement and assessment (even sometimes of development) are therefore possible in the description of system thermal hydraulics during this phase.
5. SYSTEM THERMALHYDRAULICS DURING RELOCATION INTO THE LOWER PLENUM AND DURING LOWER HEAD FAILURE

5.1. Phenomena to be described

Core relocation in the lower plenum depends very much on the core slumping scenario inside the core. The only experience we have is the TMI2 accident and the degradation models inside the core are neither detailed nor assessed to sufficiently provide anything but expectations. What will occur in the lower plenum is a function of:
- the amount of water remaining in the lower plenum at the time of corium relocating,
- the corium flowrate
- the geometrical pattern of corium flow

The amount of water remaining in the lower plenum depends on the balance between injected water which may come through the downcomer and water evaporated at the surface. The mechanisms that provoke this evaporation are the conduction from structures in contact with the crust in the core, and radiative heat transfers from the above hot structures. These mechanisms are clearly a function of the core degradation scenario. The remaining amount of water will influence the capability of the water to quench the corium jet.

The corium flowrate into the lower plenum depends on how the crust fails (thermal or mechanical failure; center or edges of the core; large, small, distributed or local failure?). As a result two categories of lower plenum scenario may be considered. In the first, the corium flowrate is high enough for a significant part of liquid corium to reach the bottom of the lower head unquenched. The corium can then directly interact with the walls and penetrations: the lower head failure may occur in a few minutes and water plus corium fall into the containment cavity. In the second the corium flowrate from the core is not as high and quenching occurs in the lower plenum water. A crust/debris forms on the bottom of the lower head. These debris will have to remelt before the failure of the lower head. At the same time they will vaporize water. Lower head failure therefore occurs much later than in the first case. These differences in scenario also affect the chemical content of the mixture which falls into the containment cavity and especially the amount of unoxidized metals.

The penetration of corium into the lower plenum water may induce fuel coolant interaction. This interaction depends on the corium dispersal state which is clearly a result of the geometrical flow pattern and which may be influenced by splashing effects on the structures of the lower plenum. This interaction may induce a steam explosion and at least a large steam production peak.

Safety issues mainly concern the timing of vessel failure. This timing depends on the thermal hydraulics of cooling and possible quenching of the falling corium jet and on the thermal behaviour of the fallen debris in contact with vessel walls and water pool. Fuel coolant interaction could be also of concern mainly for the possible risks of mechanical failure of the structures.

5.2. Available physical models and codes: needs for improvements and assessment

Very few codes are able to predict this phase /12/. In short, the prediction of the relocation of corium into the lower head requires the modelling of:
- two-component (corium water), multiphase (liquid, gas and solid) thermal hydraulics,
- at least 2D geometries for heat transfers and flows,
- thermal hydraulics of fuel coolant interaction,
- thermal and mechanical behaviour of vessels walls in contact with corium.

Except for the last model, the requirements are mainly in the thermal hydraulics field. Partial information is available for some individual phenomena, but their topological coupling requires a great deal of development. Concerning fuel coolant interaction, some mechanistic models are being developed, but there remains much to be done for the improvement and assessment of these models. Here again large areas of development, improvement and assessment are possible in order to describe system thermal hydraulics more accurately.

6. SYSTEM THERMALHYDRAULICS DURING FISSION PRODUCT RELEASE

6.1. Phenomena to be described

The initiating phenomena for the release of Fission Products are:
- fuel temperature,
- kinetics of the temperature transient.

The phenomena which will influence F.P. release are:
- the chemical environment (for example oxidizing or reducing conditions),
- the fuel state (degree of irradiation, fragmentation,...)
- control rod and structural material behaviour (acting on the physical and chemical state).

The thermal hydraulics phenomena to be described are mainly the evolutions of flows and temperatures inside the core. The needs are then exactly the same as those discussed for predicting core boil off, core heat up, core oxidation and core relocation. Besides the needs for describing thermal hydraulics in the circuit which are essential for obtaining the right vessel boundary conditions, these needs include the description of 3D core flows and heat transfers taking into account radial heterogeneity and blockage evolution due to core degradation.

Safety issues are obvious. It is the initial source term which is the issue. Notwithstanding the need for an accurate local fuel release model, the temperature map of the core and its evolution throughout the transient is a key parameter for source term evaluation. The flows which determine heat transfer, not only condition the temperatures but also the local fluid contents (non-condensible gases, hydrogen, oxidizing or reducing chemical state) which are essential in the process of F.P. release.

6.2. Available physical models and codes: needs for improvement and assessment

The same discussion as in §3.2. and §4.2. applies here. In the two-tier approach, the integrated codes with simplified models give approximative results whose accuracy decreases significantly as the transient progresses. Detailed mechanistic codes are in a better position and are practically indispensable; but when advanced core degradation and relocation occur, their accuracy becomes problematical and they still need extensive improvements and assessment.
7. SYSTEM THERMALHYDRAULICS DURING FISSION PRODUCT TRANSPORT, DEPOSITION, REEVAPORIZATION

7.1. Phenomena to be described

The accurate prediction of the behaviour of the carrying fluid is obviously a necessary condition for describing Fission Product transport and all the related phenomena. The parameters of the flow which are needed can be classified into three categories, taking note that strong interactions exist between them. These are the mass parameters (concentration), the mechanical parameters (gas flowrates or velocities) and the thermal parameters (gas and wall temperatures). Interaction between them is due to heat transfers (heat transfer inside the flow, heat transfer between the flow and the walls) and mass transfers (condensation, chemical absorption for example).

The mechanical behaviour of the mixture such as aerosol agglomeration, settling or impaction is mainly related to the mechanical parameters of the flow (velocities). The thermal behaviour such as formation of aerosols, condensation on aerosols or on walls is related to the thermal parameters (gas and wall temperatures). For chemical behaviour all parameters interact more or less: the mass parameters through chemical species concentration, the mechanical parameters through the residence times, the thermal parameters through dependence of chemical reactions on temperatures. Varied coupling occurs between the different phenomena: for example the influence of the residual heat of the deposited aerosols on thermal behaviour or the influence of condensation on aerosols on the chemical reactions /13,14/.

The thermalhydraulic phenomena that require describing are first those concerning the circuit; however they clearly depend on core thermalhydraulics as this provides the boundary conditions for the circuit. The needs are the same as those discussed for the phases of core heat-up, degradation and relocation. The phenomena include natural circulation in the circuit, with liquid stratification (water plugs), countercurrent flows (water capture in some components), effect of non-condensable gases on hydraulics and heat transfer, 3D flows in the core, recirculations in hot legs and steam generators, oscillating flows,.....

The safety issue is both simple and important: the amount of F.Ps released in the containment and which could participate in an eventual external release. This issue is clearly essential in the case of containment bypass.

7.2. Available physical models and codes: needs for improvement and assessment

The same discussion as in § 3.2. and § 4.2. applies here, but must be more developed regarding circuit behaviour aspects, as the transport, deposition, and revaporization of F.Ps depend directly on the thermalhydraulic parameters of the circuit, whereas for core evolution (§3.2.4.2.) these circuit parameters only play an indirect role albeit essential.

For the available models and codes the same distinction as above has to be made between the integrated codes and the detailed mechanistic codes. This distinction is first based on the physical modelling level. For this level it is clear that detailed mechanistic codes even in the version for conventional accidents can provide much more adequate detail than the integrated codes. Secondly this distinction concerns the degree of detail of the topological description. For integrated codes coarse meshing is always used. For mechanistic codes and for F.P transport evaluation, because of the extremely long computing times of the aerosol codes, meshing is often reduced or averaging procedures are used. In this case the evaluation of the mechanistic codes is not very different from that of the integrated codes, and with the corresponding amount of uncertainties.
There is clearly a contradiction between the sophistication of aerosol codes which take into account different species, different size classes, detailed physical phenomena (thermophoresis, diffusiophoresis, coagulation, adsorption on the walls,....) and the use of very coarse spatial meshing required by the numerical performance. The need of sophistication for the aerosol aspect in fact favours detailed mechanistic codes. But progress should be made from this aspect (especially on numerics) in order to fully benefit from thermalhydraulics details. The coupling of thermalhydraulics and aerosols should be also improved. Besides these first priority improvements for F.P. evaluation, the same models improvements for thermalhydraulics as discussed earlier, should also remedy here insufficient phenomena prediction. The models concerned are:

- countercurrent flow limitations
- recirculation modelling in pipes and steam generators
- heat transfer modelling
- condensation modelling especially in the presence of non-condensable gases.

8. S.A. SYSTEM THERMALHYDRAULICS: FUTURE USE AND RESEARCH

The analysis that has been made of thermalhydraulic phenomena shows that in every severe accident phase, system thermalhydraulics plays a significant role. During the initiation phase, event timing is determined by a correct mass inventory which requires an accurate mass distribution evaluation. When the core starts to degrade and during all the following degradation phase, mass distribution and energy transport are the dominant thermalhydraulic phenomena which again require a detailed description of both mass distribution in the circuit and flow behaviour inside the core. For F.P. release and transport a good knowledge of thermalhydraulic parameters all through the primary circuit is needed. Therefore, a detailed thermalhydraulics description is desirable throughout the whole severe accident. For the future, the following trends may then be expected in the use of thermalhydraulics in S.A. and in the desirable research directions:

- First an extension of the use of advanced thermalhydraulics codes available today is possible and will considerably improve severe accident predictions which are made at present with integrated codes.
- A second step for better S.A. prediction should be the addition of improvements to the existing codes. To summarize the preceding chapters, improvements of interest are numerics optimization during the initiation phase (also interesting for other non-severe accident situations), better physical laws for when non-condensable gases are present (also interesting in non-S.A. situations), better CCFL and condensation laws (also interesting in non-S.A. situations), better 3D core models (also of general interest), adaptation of heat transfer packages to high temperatures situations.
- A next step should be the modelling of degraded core reflooding for which the present status is relatively poor.
- The relocation of corium in the lower plenum is a domain where much development is still to be made with multicomponent-multiphase flow models and fuel coolant interaction models.
- Even if thermalhydraulics problems alone have been emphasized here due to the subject of this paper, it has to be acknowledged that this is only one part of the S.A. prediction problem. As one part but an indispensable one, it has to be coupled with other phenomena and this coupling is certainly one of the greatest challenges in S.A. modelling for which efficient solutions have to be found.
9. CONCLUSION

Analysing severe accidents from the system thermalhydraulics view shows S.A. phenomena in a different light from that most usually observed. The importance in severe accidents of mass inventory, mass distribution and energy transport are thus naturally highlighted. Prediction of these leading phenomena appears clearly as necessary in order to define core degradation phenomena, the first event to make the accident enter into the severe accident category; it is also necessary for determining the thermodynamic states that govern fission product behaviour (release and transport).

Therefore, in the future areas for system thermalhydraulics investigation, the use in the severe accident domain of the best estimate thermalhydraulic advanced codes certainly appears to be very advantageous. Some improvements, of which many are not specific to S.A.s, should answer questions in which we must recognize that there are to date no satisfying answers. For some portions of the transients (reflooding and lower head behaviour for example) development is even necessary. Contrary to what has been assumed in some S.A. approaches there is no opposition between thermalhydraulics, as it has been defined in the conventional accident analyses and severe accident studies. Continuity between the two domains as already is apparent in some mechanistic approaches (identical code version in non-severe and in severe accident analyses) is certainly the best way to valorize past thermalhydraulics studies and to meet as satisfactorily as possible future safety needs.

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AN INHERENT BORON DILUTION MECHANISM
IN PRESSURIZED WATER REACTORS

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ABSTRACT

The behaviour of boric acid during natural circulation
together with Small Break LOCA experimental data shows
that in present-day pressurized water reactors (PWR) there
exists an inherent mechanism which can accumulate boron-
free condensate in the cold leg loop seals. During reflux/
boiler condensation, the accumulation rate is typically
about 1.6 m³/min for a 1300 MWe UTSG plant. This
observation, combined with the results of analyses of
externally caused boron dilution transients, suggests that
SB LOCA could become a reactivity induced accident (RIA)
initiator. The severity of the reactivity accident
potential is yet to be quantified.

1. NATURAL CIRCULATION PHENOMENA

Inherent boron dilution can occur whenever the decay heat
is removed from the core by phase-separating natural
circulation, that is, by reflux condensation in case of
vertical U-tube steam generators (UTSG) and by boiler
condensation in case of horizontal or vertical once-throug-
ough steam generators (OTSG). These conditions lead to
boron dilution because 1) boric acid does not markedly
dissolve into steam (see below) and 2) boron-free
condensate can accumulate in a subspace of the primary
circuit, the loop seals (figure 1). The existence of such
a generic mechanism has been suggested\(^2\) and the
reactivity consequences have been studied\(^2\)\(^8\), without
quantifying the thermal-hydraulic prerequisites in detail.

The solubility of boric acid in steam is usually
represented in terms of distribution coefficient \(K\),
deﬁned as the ratio of solubility in vapour to
solubility in liquid. According to\(^3\) the distribution
coeﬃcient can be calculated from

\[
K = \left( \frac{\rho_v}{\rho_L} \right)^n
\]

where \(\rho_v\) and \(\rho_L\) are the densities of vapour and liquid,
respectively, and \(n = 0.88\)\(\ldots\)1.2. The most appropriate
value over the range of interest in LWR safety analysis
seems to lie on around the lower bound. See table 1.
Table 1. Boric acid distribution coefficient $K$.

<table>
<thead>
<tr>
<th>Temperature</th>
<th>394</th>
<th>450</th>
<th>477</th>
<th>533</th>
<th>589</th>
<th>616</th>
<th>647 (K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pressure</td>
<td>2.0</td>
<td>9.3</td>
<td>17</td>
<td>47</td>
<td>107</td>
<td>151</td>
<td>220 (bar)</td>
</tr>
</tbody>
</table>

$K$, Ref. 4, as referred to in $^5$

0.025  0.033  0.040  0.056  0.095  0.150  1.00

$K$, Equation (1) with $n=0.9$

0.002  0.009  0.016  0.043  0.11  0.20  0.82

Processes that could enhance boron transport, such as extensive liquid entrainment, exist but are weak enough to allow a study assuming essentially no boron transport with steam. The distribution of steam condensation in vertical U-tubes has been measured in Semicale$^6$, BETHSY$^{10,11}$ and KWU/Singletube experiments$^9$. The result is that under saturated conditions condensation is split 1:1 between U-tube upflow and downflow sides. The downflow side contribution increases with steam velocities exceeding the flooding limit at tube inlet$^9$ and with steam superheating$^{10,11}$.

Figure 1. Primary geometry and condensate flows during steady-state phase-separating natural circulation.

Reflux condensation is characterized by a low but positive mass flow. Consequently, very little mixing between the
accumulated boron-free condensate in the loop seal and higher-concentration liquid in the pressure vessel can be expected. Because of the limited volume of the loop seals, dilution of the boron concentration can proceed rapidly even at small mass flows.

Experimental data for horizontal steam generators\textsuperscript{14,16} is somewhat ambiguous at the moment. However, data from REWET-III facility\textsuperscript{14} and computer simulations\textsuperscript{15} suggest that most of the condensate flows towards the cold leg.

Experimental data on the behaviour of vertical U-tube steam generators\textsuperscript{8-13} is in general very unanimous, giving similar sequence of various natural circulation modes and heat transport mechanisms at variable primary inventories (see figure 2). (Note that the pressurizer is not included in these inventories. See\textsuperscript{6-13} for details.) However, the transition between different circulation modes, especially two-phase to reflux, have been quantified to limited accuracy only and vary considerably from facility to facility. For example, the onset of fully developed reflux circulation occurred at 80% primary inventory in early PKL experiments\textsuperscript{7}, whereas most other reports\textsuperscript{6,9,12-13} indicate inventories around 60-70%, and BETHY\textsuperscript{10,11} even around 50%. This discrepancy arises from geometry differences between reference plants, scaling distortions of various components in the facilities and perhaps from different interpretations of the results. (The transition from two-phase circulation to reflux condensation can appear as a relatively gradual process.)

![Diagram](image)

Figure 2. Natural circulation mass flow rates, normalised to the single-phase saturated flow, at varying primary inventory. Core powers are indicated in percentages of nominal.
The effects of inclusion of the pressurizer in the plant analysis are two-fold. Comprising typically 10-15% of the whole primary volume, the pressurizer can act as a large water storage which either removes coolant from the circuit due to a leaking relief valve, for example, or suddenly adds coolant to the circuit as the leaking valve is closed. Furthermore, the inventory criteria that distinguish between various natural circulation modes must be modified according to the expected behaviour of the pressurizer (which, in turn, may be difficult to assess. Consider, for example, the TMI analysis experience[25]).

1.1. Dilution rate

The dilution rate for fully developed steady state phase-separating natural circulation can easily be calculated from the decay heat removal requirement. The power and latent heat in the primary pressure give the natural circulation required mass flow. The measured flow split (1:1 in UTSG, no split in OTSG, in between in horizontal) and total loop seal volume (liquid mass) are used to obtain pure liquid accumulation and boron dilution rate in the loop seal. Two bounding cases exist: no mixing in the loop seal (pure liquid displaces the original liquid), and full mixing (pure liquid replaces dilute mixture).

In case of fully developed reflux/boiler condenser circulation the steam mass flow \( q_{m,s} \) required to remove the decay heat \( P_{dec} \) is

\[
q_{m,s} = \frac{P_{dec}}{1 + \Delta h}
\]  

(2)

where \( \Delta h \) is the latent heat and \( \Delta h \) core inlet subcooling, (now zero but kept along for later convenience). Condensate mass flow into RCP loop seal (accumulation rate) depends on the flow split, or accumulating fraction, \( f_a \)

\[
q_{m,a} = f_a \cdot q_{m,s}
\]

(3)

Accumulating fractions for generic steam generator geometries are given in table 2.

Table 2. Fraction of condensate that accumulates in the RCP loop seals (crossover legs).

<table>
<thead>
<tr>
<th>SG type</th>
<th>( f_a )</th>
<th>reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>UTSG</td>
<td>0.5</td>
<td>6,9,10,11 (experiment)</td>
</tr>
<tr>
<td>OTSG</td>
<td>1.0</td>
<td>17 (experiment)</td>
</tr>
<tr>
<td>Horizontal</td>
<td>0.68</td>
<td>15 (calculated)</td>
</tr>
</tbody>
</table>

If no mixing occurs in the loop seal, pure condensate displaces the initial liquid at rate \( V_p(t) \)
\[ V_p(t) = (q_{m,a} \rho) \cdot t \]  \hspace{1cm} (4)

where saturation in the loop seal is assumed at all times.

If the loop seal is optimistically assumed completely mixed at all times, the boron concentration decays from the initial value \( C_0 \) as

\[ C(t) = C_0 \cdot \exp(-k_2 \cdot t) \]  \hspace{1cm} (5)

The decay coefficient \( k_2 \) and the corresponding concentration half-life \( T_{1/2} \) are

\[ k_2 = \frac{q_{m,a}}{3V_{LS}} \]  \hspace{1cm} (6)

\[ T_{1/2} = \frac{\ln 2}{k_2} \]  \hspace{1cm} (7)

In present-day large PWRs thermal powers are typically around 3000-4000 MW, loop seal volumes 6-10 m\(^3\) in each loop and core coolant volumes within 15-20 m\(^3\), implying that even if only two or three loops (out of four) participated in the dilution process, the diluted slugs could still fill the whole core.

For example, for a 3500 MW UTSG plant at 2% decay power (corresponding to operation between 15-35 minutes from scram) with 20 m\(^3\) total loop seal volume and 50 bar reactor pressure the condensate accumulation rate is about 1.64 m\(^3\)/min. Consequently, if no mixing takes place in the loop seals, 20 m\(^3\) of boron-free liquid will be generated in 12 minutes once the reflux condenser circulation has commenced. On the other hand, if the loop seal were completely mixed at all times, the concentration would decay with half-life \( T_{1/2} = 505 \) s = 8.5 minutes.

The above analysis is valid for closed systems only, as it neglects the energy removal due to breaks and leaks and heating from structure cooldown. These are discussed in more detail in chapter 2.

2. SB LOCA CONSIDERATIONS

Small break LOCAs have been a subject of intensive study since the TMI-2 incident. This chapter utilizes the results of (mostly recent) SB LOCA experiments in downscaled experimental facilities PKL-3, Semiscale\(^{16}\), LOBI-MOD2\(^{19}\), ROSA-IV, LSTFZ0-21, and BETHSY\(^{22}\). These experiments form a background against which the relevance of the dilution phenomenon detailed in chapter 1 can be assessed. In particular, this chapter identifies the approximate break size spectrum in which inherent dilution always occurs. However, since SB LOCAs result in much more complex behaviour of the plant than the steady-state
natural circulation experiments discussed in chapter 1, only generic trends are discussed. The details of the system behaviour depend on plant geometry, break and injection locations, ECC capacities and equipment failure criteria, all of which differ considerably in various designs. Comments on the TMI-2 incident are also given from boron dilution point of view.

2.1. Generic SBLOCA phenomenology

The experimental data indicates rather clearly that there is a range of leak sizes where the energy removal via the break flow is not sufficient to remove all of the decay heat. This range lies within 0.5-2% leak region where percentages refer to the cold leg flow area and translate to about 20-80 cm² in a typical large PWR with 0.75 m ID cold legs. Consequently, it is crucial that secondary side is available as a heat sink, in order to remove the decay heat and depressurize the primary so that long-term cooling can be effected.

The break mass flows, especially in case of cold leg breaks, exceed the high pressure safety injections (HPSI) during the early phases of SB LOCA. As a consequence, the primary inventory can be depleted to the point of reflux/boiler condenser initiation before the leak and injection mass flows stabilise. As soon as reflux/boiler condenser circulation sets in, boron dilution begins.

Fortunately, the mass flow rate from core to the SG's can be lower than in case of "fully developed" circulation discussed in chapter 1. This is due to 1) the colder HPSI liquid which also participates in the core cooling and depressurisation process and 2) the break which removes a fraction of the energy in the primary. However, the magnitudes of these reductions are strongly both plant- and scenario-specific. Furthermore, they are partially offset by the need to cool down the primary (including structures) in addition to removing the decay heat. As the boron dilution rate is a linear function of the power to be removed, one can estimate that the dilution times double when the required secondary cooling is halved.

Experimental data from UTSG facilities and 0.5% cold leg breaks gives the general behaviour in roughly four phases:

- **Phase 1:** Depressurization to saturation. This takes usually 5-10 minutes, during which single-phase discharge prevails.
- **Phase 2:** Two-phase natural circulation and transition to reflux/boiler condenser mode. This takes roughly 5-10 minutes more, depending on the actions on secondary side.
- **Phase 3:** Reflux/boiler condensation, during which the primary can be depressurized efficiently by
secondary cooling. This phase extends up to the accumulator injection, which usually occurs at around 40 minutes after the break opening. Because reflux/boiler condensation is a very effective heat transport mechanism, the primary pressure tends to follow the secondary pressure closely. Boron dilution takes place during this period (typical duration 20-30 minutes).


The timings above seem to diminish almost linearly as the break size increases. For a 2% break boron dilution could still continue for at least 5-8 minutes. The dilution rate also decreases due to the faster depressurization.

Note that the assessment of ultimate fate of the dilute slugs during phase 4 is beyond the capabilities of the present-day system codes (RELAPS, TRAC, CATHARE and the like). These codes exhibit far too much numerical diffusion to be useful for tracking of a relatively sharp concentration gradient around the system.

2.2. Loop seal clearing

An aspect not captured by the steady-state analysis of chapter 1 is created by loop seal clearing effects. Experimental evidence shows that during continuous, uncompensated inventory loss (HPSI failure or larger, 5% break, for example), steam passing through the SG's can blow away the liquid from the RCP loop seals. Even if this did not occur, the available liquid volume in the loop seals would decrease due to break-induced manometric effects (higher liquid levels at vertical sections near the break) and possibly steam pull-through. This would decrease by a factor of about 2 both the time required to accumulate a "largest possible" plug and size of the plug.

Early loop seal clearings would practically ensure that the condensate accumulates unmixed (this has been observed in ROSA-IV LSTR$^{24}$) and is consequently of lowest possible concentration. Late loop seal clearings on the other hand could blow the accumulated mass of boron-free water into the downcomer. Whether the latter is advantageous or not is unclear at the moment.

2.3. The refill phase

The problem is that eventually the primary circuit will be gradually refilled, and during this process the two-phase
natural circulation mode could take over again. If any dilute slugs remain present in the primary when the two-phase natural circulation begins, the dilute plug could get into core relatively unmixed and perhaps with considerable velocity. As of yet, there is little experimental data to definitively support or refute these considerations.

One could argue that the cold leg ECC injection mixes with the plugs in the loop seals, eliminating them. This seems rather improbable as long as steam pull-through continues because countercurrent flow limitation in the pump prevents the liquid fallback. Clues about the efficiency of the injection mixing processes in a calm system can be obtained from experiments performed in connection to the pressurized thermal shock (PTS) evaluations. Creare data indicates that only about 17% of the ECC flow went towards the loop seal in a stagnant system. This allows evaluation of the loop seal mixing time constant: for a 4 m³ plug (in a 8 m³ loop seal) and 30 kg/s ECC flow one obtains concentration doubling in about 9 minutes. Unfortunately, as the plug is less dense than ECC, large fraction of the plug escapes back towards the SG unmixed, forming a stably stratified "pocket" in the SG outlet chamber and the descending part of the loop seal.

At low pressures the primary refill proceeds quite rapidly, and two-phase natural circulation would tend to start according to figure 2, as long as the secondary heat sink is available. BETHSY data (with lower plenum refill which should give comparable results with cold leg refill) indicates no hysteresis effects for the natural circulation flow rates at decreasing versus increasing inventory, but shows that the mode transitions occur at slightly different times in different loops. (For hot leg injections, considerable hysteresis can be expected.)

Because the typical single- and especially two-phase natural circulation flow rates can be quite high, in excess of 1200 kg/s near the maximum, the dilute plugs, once set in motion, could proceed to the core without becoming sufficiently borated by ECC. The most effective mechanism that could mitigate the dilution is turbulent buoyancy-induced mixing that the plug experiences as it passes through the downcomer. The efficiency of this mixing process depends strongly on the conditions in the primary and plant characteristics. Preliminary analyses suggest that the mass flow required to push the negatively buoyant plug down through the downcomer could under unfavorable conditions be even less than typical single-phase natural circulation flow. Because the core of a negatively buoyant plume experiences little mixing, the risk of introducing almost boron-free liquid in the reactor core is evident. Experimental data is required to quantify this risk conclusively.
From the reactivity studies' point of view introduction of boron-free coolant into the core under low primary pressure and temperature is somewhat complicated. Low pressure implies easier boiling but low temperatures imply much higher liquid densities. These counteract each other in terms of reactivity influence. Furthermore, as the fuel cools down, the shutdown margin tends to decrease due to the Doppler effect.

2.4. TMI-2 incident

TMI-2 is the only case so far where inherent boron dilution could occur in real plant. It is known that boiler-condenser natural circulation took place during the mid-phase of the transient. This should have had dilution as one consequence, if the analysis of chapter 1 is correct. The operation of one RCP at later phases could then have driven a dilute plug into the core, causing consequences beyond what was actually experienced. Fortunately, the potential for a reactivity accident did not actualise. In the following it is shown that this outcome is not in contradiction with the dilution mechanism described in this paper.

According to\textsuperscript{24}, the primary system transitioned to boiler-condenser natural circulation at about 101 minutes into the transient when the A loop RCPs were tripped. However, the B loop steam generator did not participate in the process and the natural circulation in A loop ended at 128 minutes. The energy removal by the leaking PORV and the makeup/letdown unbalance during that time can be estimated by data in\textsuperscript{25} and is at most 30\% of the decay power. The rest (including the depressurization that occurred) had to be removed by natural circulation in loop A. Consequently, dilution could take place for 27 minutes, producing at least about 24 tn of boron-free coolant into the loop A loop seals. The operation of one RCP between 174 to 200 minutes took place in B loop. Hence, the dilute plug could not have been driven to the core by the pump restart in TMI. Further analysis on the issue would undoubtedly prove fruitful.

3. REACTIVITY CONSEQUENCES

In the present-day PWR's boron is the largest contributor in terms of reactivity worth and exceeds clearly the control rods at the beginning of core life. During the operation cycle the boron concentration decreases linearly towards zero at the end of core life. Consequently, flushing the core with pure liquid represents a quite considerable reactivity insertion potential during the first half of the core life.

Studies on externally caused boron dilution with small plugs\textsuperscript{28,29} (500 kg in\textsuperscript{28}) suggest severe core damage.
Similar analyses have been performed assuming various plug sizes but higher concentrations\textsuperscript{27}. A semi-infinite slug with a drop of 750 ppm from initially 1500 ppm concentration resulted in an excursion that breached the RIA criteria. The main conclusion of\textsuperscript{27} is that over 2/3 of the total reactivity yield of a propagating plug is gained over the first 1/3 of core length (see figure 3). This result is relatively insensitive to the form of the leading edge of the plug.

\begin{figure}[h]
\centering
\includegraphics[width=\textwidth]{reactivity_insertion.pdf}
\caption{The reactivity insertion as function of diluted front axial position\textsuperscript{27}. Node 0 is core bottom, node 13 core top.}
\end{figure}

However, those results are not directly applicable here because the analysts prescribed an inadvertent reactor coolant pump restart and consequently obtained somewhat higher INSURGE velocities than natural circulation can produce (although the gap between peak two-phase mass flow and the steady core throughput of only one RCP is small, especially in four-loop plants). At smaller inflow velocity boiling can take place in the core, reducing the reactivity. Consequently, the ultimate reactivity effect depends strongly on the slug density (temperature), too.

At the time of natural circulation restart after a SB LOCA, there could well be more than 2-3 m\textsuperscript{3} of pure liquid left in each loop in the steam generator inlet chamber and loop seal descending part. Further analysis on the reactivity effects of dilute slugs entering the core at intermediate velocities are highly desirable.

Severe core damage would also result if a slug entered the core at very low velocity\textsuperscript{30}. At small velocities so-called chimney effect arises in the core, leading to two
consecutive power excursions. The first excursion is bottom-peaked and solely due to the upward propagating dilute front; the second arises as the heat liberated at lower part of the core is transferred to the coolant, causing boiling which pushes the front rapidly to the cool upper part of the core, causing the second, even more energetic, excursion. Thorough analysis of such events demands at least 1-D kinetics and a hydraulic model with flow reversal capability.

Fortunately, the buoyancy and turbulence induced mixing processes along the way from the loop seal to the core may sufficiently increase the concentration of slowly moving slugs to prevent disastrous outcome. As noted earlier, this should be experimentally verified. Recall from chapter 2 that the analysis of dilute slug movements is beyond the capability of currently available system codes.

A detailed, preferably 3-D neutronics study should also be performed as soon as the core inlet conditions are known with confidence.

4. OTHER TRANSIENTS AND ACCIDENTS

In addition to the classical small break LOCAs dealt with in chapter 2 there exist other transient scenarios where inherent boron dilution is possible. This chapter considers four such scenarios: a pressurizer relief or safety valve leak, a steam generator tube rupture, a loss of feedwater ATWS and a general severe accident scenario. These scenarios are discussed only qualitatively here. This list of transients should not be regarded as exhaustive. In general, all transients and accidents involving a period of operation under reflux/boiler condenser operation have the potential for inherent boron dilution.

4.1. Pressurizer relief/safety valve leaks

One of the most likely candidates for inherent dilution transient is a pressurizer relief/safety valve leak, which could lead to reflux/boiler condenser circulation at considerably higher total primary inventory than the other LOCAs. This is due to the relatively large volume of the pressurizer, which would get filled with liquid sucked from the circuit during the early phases of valve leakage. Of particular importance is the risk of flooding the primary circuit from the pressurizer as a consequence of leak isolation. Such an event could easily bring the primary from reflux/boiler circulation to two-phase natural circulation and provide an effective mechanism that drives the dilute plugs into the core.
4.2. Steam generator tube ruptures

Semiscale evidence\(^1^8\) indicates that concurrent rupture of multiple SG tubes (break areas corresponding to 1, 5 and 10 tubes were used) can also lead to primary inventory depletion exceeding the reflux initiation threshold. Similar results have been obtained also at LOBI-MOD2, with break area corresponding to about 3 U-tubes\(^2^2\). The eventual outcome of such a transient is, however, strongly dependent on plant characteristics and operator actions. In the absence of effective HPISI and perhaps combined with secondary cooldown the SGTR could also lead to accumulation of significant quantities of diluted coolant. Should this occur, the mitigation of the accident would become complicated. In this context the risk of pure water ingress from the secondary side should also be taken into account, if the primary pressure is reduced below the secondary pressure.

4.3. Loss of Feedwater ATWS

The loss of feedwater ATWS transients are characterised by very high pressures in the primary. The problem is that during the initial phases of these transients a large quantity of primary inventory is lost through the pressurizer safety valves. The secondary inventory is also lost, perhaps completely. As the reactivity feedbacks shut the reactor down, intense boiling takes place in the core. If the primary inventory loss has been sufficient, reflux/boiler condenser circulation will remove the decay heat to steam generators fed with auxiliary/emergency feedwater only. The problem is that the heat transfer from the primary to the secondary may not be sufficient to cool down the primary soon enough; reflux/boiler condenser circulation could take place at primary pressure near the nominal conditions. This implies quite considerable speedup in the dilution rates, because the latent heat decreases rapidly in pressures over 100 bar. Furthermore, injective counteractions may be difficult or even impossible because of the high pressure. Should the primary eventually cool down sufficiently to allow inventory recovery, the reactivity effect of the diluted plugs would be magnified due to the absence of control rods in the core. (A noteworthy fact for all the other scenarios involving control rod malfunctions, too.)

4.4. Severe accidents

Inherent dilution can also occur during severe accidents. Coolant loss far below the reflux/boiler initiation limit is a prerequisite of the core heatup stage. This allows reflux/boiler circulation to take over as soon as heat sink is restored even temporarily (by accident management measures such as secondary feed-and-bleed, for example).
This would again cause rapid dilution in the loop seals.

Although there does not seem to be an imminent risk of diluted slugs entering the core as long as primary inventory remains low, a sudden flooding cannot be ruled out when inventory is regained, for example by accumulator injection as a consequence of primary depressurization. If the core geometry has not changed extensively before the recovery, the possibility of flooding-induced criticality seems to be present.

5. CONCLUSIONS

Based on experimental knowledge of natural circulation phenomena as well as SB LOCAs and a very simple energy balance analysis it has been shown that

1) an inherent mechanism for boron dilution in the RCP loop seals exists for most transients and accidents that involve heat removal by reflux/boiler condensation natural circulation;

2) under conditions which typically prevail during 20 to 80 cm³ SB LOCAs, the loop seal boron-free coolant accumulation rate is of the order 0.5...2 m³/min, depending on the break energy removal capacity and high-pressure ECC capacities;

3) the duration of the dilution process under typical SB LOCA conditions can exceed 20 minutes, depending on the operator actions taken;

4) the minimum total volume of boron-free coolant in a is about 3 m³/loop seal, even after the ECC mixing has been accounted for.

Taken together with the high probability of natural circulation restart during primary refill and the results of earlier dilution studies one can conclude that SB LOCA, though mitigated according to the currently accepted practice, leads to a significant reactivity accident risk. The inherent dilution mechanism is in no way limited to SB LOCA's, however, but is present whenever reflux- or boiler-condenser natural circulation is relied upon. Clearly, countermeasures effective enough need to be found.

The most obvious (and effective) countermeasure would be the reduction of excess reactivity compensated by boron. This would involve e.g. extended use of fixed burnable absorbers in the core.
Further studies should at least
1) study the dilute plug movements and mixing possibilities underway to the core at velocities typical to single- and two-phase natural circulation;
2) quantify the reactivity insertion response spectrum in more detail, that is, determine the concentrations that lead to criticality, prompt criticality, fuel failures and fragmentation, including analysis beyond the first power peak. This would include quantification of the chimney effect that can arise in the core-downcomer U-tube system;
3) analyse local criticality effects to be expected when all the loops have not participated in the dilution process;
4) recognise and quantify all the transient scenarios that possess inherent dilution potential;
5) revise the emergency operating procedures, if necessary;
5) determine the effectiveness of the countermeasures to be devised.

STUK is currently performing studies on these areas.
REFERENCES


15. H. Sonnenburg, "Modelling of the Horizontal Stack..."
RELEVANT THERMALHYDRAULIC ASPECTS OF NEW GENERATION LWR'S

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ABSTRACT

The present paper deals with the evaluation of thermalhydraulic aspects retained of importance for the assessment of safety of the new generation nuclear plants.

Following a survey of the reactor concepts proposed for the future, the attention will be focused toward SBWR, AP-600 and PIUS whose characteristics, under many respects, bound the features introduced in the largest part of the new reactors.

Expected relevant phenomena typical of the mentioned plants will be discussed in the paper; on this basis a critical overview of the experimental activities planned or in progress is presented and a judgement about the suitability of available computer codes is formulated.

Conclusions are drawn in relation to the assessment of the new design proposals from a thermalhydraulic point of view.

1. INTRODUCTION

The Light Water Reactors (LWR) are considered primary nuclear reactors for power generation in the first half of 21st century. Nevertheless, the safety of the systems must be continuously improved to cope with the increase in the capacity of the electric power generated by the nuclear source in developing countries as well as in developed countries and to preserve the environment on the earth at the same time.

In this context, the public demand for improved safety and the utilities request for increased economic profit, also looking at investment cost, led the vendors to the proposal of advanced reactors. In the domain of LWR, two main classes can be distinguished:
- evolutionary reactors (e.g. Advanced BWR, System 80+ PWR, etc);
- innovative reactors (e.g. Simplified BWR, Advanced PWR-600, PIUS, etc.).

In the former class, the systems and the functions of the present generation reactors have been fully optimized on the basis of the operational experience and the current technology.

A substantial reduction of the core melt frequency together with a reduction of the investment cost obtained through the simplification of the whole system, constitute the strategic objective of the second class of reactors. Current technology shall be used in the design that will considerably shorten construction schedule. Extensive use of passive systems, especially for residual heat removal purposes, is made in this category of reactors.
The core melt frequency reduction can be estimated in the range 10-100 and significant improvement of economic performance either looking at the kilowatthour cost or at the investment, are foreseen in both classes of reactors.

From a thermalhydraulic point of view nominal and off-normal behaviour of the advanced reactors, with main regard to the above mentioned second class, may involve the occurrence of phenomena that are either of low importance or not present at all in the current generation LWR.

Following a review of the main peculiarities of the considered reactors, the main purpose of the present paper is to identify new thermalhydraulic phenomena that are expected to play a role in the operation and in the safety of advanced reactors. On this basis, considering an overview of the experimental activities in progress or planned and the experience gained in the application of available system codes to the prediction of advanced reactors transient scenarios, the need for new experiments is evaluated and a judgement is given concerning the suitability of available computer models.

2. DESIGN FEATURES OF CONSIDERED REACTORS

In order to evaluate the thermalhydraulic performance of new reactors especially looking at the possible different accident scenarios compared to the present generation reactors, two broad investigation areas can be considered:

1) comparison of the characteristic values assumed by important nominal conditions;

2) evaluation of hardware characteristics and of the intervention modalities of engineered safety features that affect the evolution of the off-normal transients.

Relevant characteristics (investigation area Nr. 1) of present generation and next generation reactor designs are summarized in Tab. 1. The considered thermalhydraulic parameters (e.g. items 6 to 9, 13, etc.) give an idea of the differences among the various concepts and of the differences expectable during transient conditions.

In the vertical columns, reactors 1 to 6 are examples of the present generation, reactors 7 to 9 are part of the evolutionary class and the remaining ones belong to the previously defined innovative category. It should be noted that the selection of the reactors in the table has been done considering the availability of data to the authors and the attempt to differentiate the various classes.

Review of the data shows that the majority of reported parameters are essentially the same for the current generation reactors and the evolutionary reactors. However in the case of the innovative reactors some values are quite different, such as the core linear power (parameter 13 in Tab. 1), the core inlet power (obtained

\textsuperscript{(*)} A important class of future LWR has not been included in Tab. 1: the high converter reactors (both PWR and BWR). These are characterized by very tight lattice, by a ratio moderator over fuel volume four times lower than current reactors and by a fuel burn up of the order of 100 Gw/ton. The design appears in a earlier stage with respect to the others, constituting the main reason for not including these reactors in the Table.
by multiplying flowrate and enthalpy) over thermal power (9), the thermal power density (18). These parameters have values quite smaller for the innovative reactors than for the present generation reactors: the new values apparently shift the nominal conditions in a direction toward the increase of safety margins.

Evaluation of the engineered safety systems shows that these are still very similar for the current generation reactors and the innovative ones. However, the extensive use of passive systems in innovative reactors (also resulting from the desire to avoid the intervention of operators for extended periods - tens of hours) led to the need of founding the reactor safety upon the availability of large heat sinks inside the containment. As a consequence, almost all the transients are forced, by automatic means, into low pressure long term cooling processes where the interaction between the primary circuit and the containment system becomes important.

For this reason and because the passive safety systems are highly interconnected to the primary circuit with feedback capabilities of the primary system on their performance, new accident scenarios are foreseeable that must be carefully studied.

In conclusion, only innovative reactors need extensive R & D studies before their commercialization. A detailed investigation of the various thermohydraulic situations requiring specific research plans in each of the innovative reactors is beyond the purpose of the paper. So only three reactors will be considered in some details: SBWR, AP-600 and FTUS. For completeness a brief description of the main features of these reactors is given below.
2.1 SBWR features

A sketch of the SBWR is given in Fig. 1 that includes the primary system (up to the main isolation valves), the containment and most of the safety features of the plant.

![Sketch of SBWR](image)

Fig. 1 - Sketch of SBWR.

The SBWR is the result of an extensive simplification of the existing BWR plant, the major simplification being the elimination of any pump to recirculate the coolant inside the vessel \(^{1/1}\). The use of natural circulation results in a simple system with less piping and less reactor vessel penetrations below top of the core. Simplifications have been introduced at several levels in the plant including the conventional part and the control room. The containment and the systems for the handling of operational transients and accidents, are described with some detail hereafter.

The containment is based on the principle of pressure suppression through the condensation of primary steam or two phase mixture in a devoted pool in the same way as current generation reactors. The core position in the vessel is at lower elevation than in present BWR increasing the margin to core uncoverage.

The basic feature of the SBWR plant is the complete integration of the containment and of the safety systems. In the majority of the foreseeable accident conditions special depressurization valves (DPV) allow the equalizations of containment and vessel pressure. This makes possible the injection of liquid into the vessel by gravity from a
special pool situated in the upper part of the containment (Gravity Driven Cooling System, GDCS). Additionally, in case of reactor isolation transients, the steam produced in the core is driven (whatever is the pressure) into a heat exchanger that transfers the thermal power to another pool on the top of the containment. The steam condensed in this heat exchanger flows back by gravity into the reactor vessel. This system is called Isolation Condenser (IC).

Additional condensers are used to remove energy from the containment drywell (Passive Containment Cooling Systems, PCCS). These condensers are located in similar pools as the IC on top of the containment. PCCS is equipped with special system that allows purging of the noncondensable gases from the condensers into the suppression pool space. The condensate from the PCCS flows by gravity into the GDCS pools. The PCCS as well as the IC and GDCS do not require any active components (e.g. pumps). The use of these systems allows also for a relatively small containment.

2.2 AP-600 features

A sketch of AP-600 is given in Fig. 2 that includes the primary system, the containment and most of the safety features of the plant.

Even in the case of AP-600, the essential technical concept underlying the design is simplification that also allows a modular construction approach. In order to evaluate the transient thermalhydraulic behaviour, three areas can be distinguished that are different from the corresponding ones in current PWR: primary circuit, containment and engineered safety features.

Apart from those outlined in Tab. I, the differences in primary
by multiplying flowrate and enthalpy over thermal power (9), the thermal power density (18). These parameters have values quite smaller for the innovative reactors than for the present generation reactors: the new values apparently shift the nominal conditions in a direction toward the increase of safety margins.

Evaluation of the engineered safety systems shows that these are still very similar for the current generation reactors and the innovative ones. However, the extensive use of passive systems in innovative reactors (also resulting from the desire to avoid the intervention of operators for extended periods - tens of hours) led to the need of founding the reactor safety upon the availability of large heat sinks inside the containment. As a consequence, almost all the transients are forced, by automatic means, into low pressure long term cooling processes where the interaction between the primary circuit and the containment system becomes important.

For this reason and because the passive safety systems are highly interconnected to the primary circuit with feedback capabilities of the primary system on their performance, new accident scenarios are foreseeable that must be carefully studied.

In conclusion, only innovative reactors need extensive R & D studies before their commercialization. A detailed investigation of the various thermohydraulic situations requiring specific research plans in each of the innovative reactors is beyond the purpose of the paper. So only three reactors will be considered in some details: SWBR, AP-600 and FTUS. For completeness a brief description of the main features of these reactors is given below.

Tab. 1 - Relevant characteristics of present and new generation reactors (evolutionary and innovative types).
2.1 SBWR features

A sketch of the SBWR is given in Fig. 1 that includes the primary system (up to the main isolation valves), the containment and most of the safety features of the plant.

![SBWR Sketch](image)

Fig. 1 - Sketch of SBWR.

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special pool situated in the upper part of the containment (Gravity Driven Cooling System, GDCS). Additionally, in case of reactor isolation transients, the steam produced in the core is driven (whatever is the pressure) into a heat exchanger that transfers the thermal power to another pool on the top of the containment. The steam condensed in this heat exchanger flows back by gravity into the reactor vessel. This system is called Isolation Condenser (IC).

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2.2 AP-600 features

A sketch of AP-600 is given in Fig. 2 that includes the primary system, the containment and most of the safety features of the plant.

Even in the case of AP-600, the essential technical concept underlying the design is simplification/3/, that also allows a modular construction approach. In order to evaluate the transient thermal-hydraulic behaviour, three areas can be distinguished that are different from the corresponding ones in current PWR: primary circuit, containment and engineered safety features.

Apart from those outlined in Tab. 1, the differences in primary
circuit concern the elimination of the loop seals; two canned rotor pumps are directly connected with the outlet plena of each steam generator and deliver the coolant to devoted cold legs (4 cold legs are part of the primary loop). Basically the containment has the same features as the current PWR. It is a full pressure steel vessel containment but, additionally it is equipped with special Passive Containment Cooling Systems (PCCS) that is sufficient to remove long term decay heat by cooling the external walls of the steel containment vessel. The cooling is provided by natural circulation of the around the containment shell and by draining water from tanks located on the top of the containment building.

The passive emergency core cooling system consists of two Core Make Up Tanks (CMTs), two large accumulators and an In-containment Refueling Water Storage Tank (IRWST). The ECC water is fed through two safety injection lines directly into the downcomer of the reactor vessel. In case of a non-LOCA accident, core cooling is provided via natural circulation Passive Residual Heat Removal (PRHR) system with an heat exchanger submerged in the IRWST. The CMT are located above the reactor coolant loop; pressure balancing lines, that open in case of accident, make possible the gravity injection of the borated liquid from the tanks to the vessel. An Automatic Depressurization System (ADS) is provided with valves connected to the pressurizer and the hot leg, that allows reduction of the RCS pressure to the containment pressure and therefore draining of the IRWST water into the RCS and establishing of long term cooling.

As indicated, the coolant inventory control and the removal of decay heat during accidents in AP600 will be achieved by using low head gravity draining or natural circulation. The low motivation forces and the multiple parallel circulation paths may lead to safety system performance disturbances due to phenomena interactions and equipment malfunction perturbances.

2.3 PIUS features

A sketch of PIUS is given in Fig. 3 from which the main features of the design can be recognized. The concept of PIUS is "more" innovative if compared with the two previous designs/4,5/. The primary coolant circulation system is immersed into a pool of cold borated water confined by a larger concrete vessel. The primary loop is connected to the borated pool at a lower and a upper elevation through so-called density locks. Within this density locks an interface between the hot primary coolant and the cold pool water is maintained via operation of the reactor coolant pumps. When the pressure drops of the flowing coolant inside the steel vessel equal the gravity head of the cold liquid in the concrete vessel, there is no flow from the large borated pool to the primary circuit. However, if a pressure balance disturbance should occur due to void formation in the core or reactor coolant pump speed drop, cold borated water from the pool will penetrate the primary system through the lower density lock leading to reactor shutdown. A natural circulation and core cooling will be established with water flowing through the lower density lock into the core, then rising through the core and the riser and exiting the primary circuit at the top of the riser through the upper density lock into the pool.
Special natural circulation loops are active to keep cooled the borated water pool and are effective to remove the decay heat.

It should be noted that the safety of PIUS does not depend on specific engineered safety systems but is ensured by features typical of the design itself.

3. EXPECTED RELEVANT NEW PHENOMENA

Thermalhydraulic phenomena relevant to the evolutionary type nuclear plant can be considered the same as those valid for the current generation reactors. A suitable review of applicable phenomena can be found in CSNI reports 132/67 and 161/77 and in NRC report 1230/82. For completeness the list is reported in Tab. II. Limited specific research activity in this area appears necessary, if one excludes new domains like Accident Management and special topics like instability in boiling channels where the interest is common to the present generation reactors.

In the case of innovative reactors the foreseeable relevant thermalhydraulic phenomena can be grouped into two categories:

a) phenomena that are relevant also to the present generation reactors;

b) new kinds of phenomena and/or scenarios.

For the category a) the same consideration apply as for the evolutionary reactors and the phenomena of concern are therefore well documented in References 6 through 8. However, it has to be noted that significance of various phenomena may be different for the innovative reactors. Nevertheless, it is believed that the data base,
| Tab. II | Relevant thermalhydraulic phenomena identified for the current generation reactors. |
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Thermalhydraulic phenomena relevant to the evolutionary type nuclear plant can be considered the same as those valid for the current generation reactors. A suitable review of applicable phenomena can be found in CSNI reports 132/'8/ and 161/'7/ and in NRC report 1230/'8/. For completeness the list is reported in Tab. II. Limited specific research activity in this area appears necessary, if one excludes new domains like Accident Management and special topics like instability in boiling channels where the interest is common to the present generation reactors.

In the case of innovative reactors the foreseeable relevant thermalhydraulic phenomena can be grouped into two categories/'9/:

a) phenomena that are relevant also to the present generation reactors;

b) new kinds of phenomena and/or scenarios.

For the category a) the same consideration apply as for the evolutionary reactors and the phenomena of concern are therefore well documented in References /6/ through /8/. However, it has to be noted that significance of various phenomena may be different for the innovative reactors. Nevertheless, it is believed that the data base,
Tab. II - Relevant thermohydraulic phenomena identified for the current generation reactors.

- Reproduction due to depressurization
- Reproduction due to boil-up
- Condensation due to depressurization
- Condensation due to heat recollection
- Interfacial, print., vent., flow
- Interfacial, print., vent., flow
- Null to fluid pressure
- Fluid. Drives at geometric discontinuities
- Pressure wave propagation
- Swelling: LDF (1)
- Valve
- Pipe
- Pipe/Volume
- Core
- Steam
- Pipe
- Branches
- Core
- Upper Plenum
- Steam
- SC-Boiler
- SC-Exit, drum (PM)
- Net leg with LCC (PM)
- Core
- Steam
- Upper Plenum
- Lower Plenum
- SC-Mix drum (PM)
- LCC in net and cold leg (PM)
- Pressure (PM)
- SC-Primary Side (PM)
- SC-Secondary Side (PM)
- Horizontal Pipe
- Core (PM)
- Pressure (PM)
- SC-Secondary Side (PM)
- Upper tie plates
- Channel inlet orifices (MIS)
- Net and cold leg
- SC Data (PM)
- Steam
- Refueling (PM)
- Upper Plenum
- Core
- Steam
- SC-Secondary Side
- Core, SC, Structures
- Core, SC, Structures
- Core, SC, Structures
- Core
- SC, Structures
- Core, SC, Structures
- Net leg
- Channel walls and water legs (MIS)
- Core, SC, Structures
- Core, SC, Structures
- Core, SC, Structures
- Core
- SC, Structures
- Core, SC, Structures
- Core
- SC-Secondary Side
- Core, SC, Structures
- Core, SC, Structures
- Core, SC, Structures
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- SC, Structures
- Core, SC, Structures
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- Core, SC, Structures
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- SC-Secondary Side
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- Core, SC, Structures
- Core, SC, Structures
- Core
- SC-Secondary Side
- Core, SC, Structures
- Core, SC, Structures
- Core, SC, Structures
- Core
understanding and modelling capabilities acquired for the current reactors are adequate for phenomena in category a).

Phenomena of the category b) can be subdivided into three classes:
b1) phenomena related to the containment processes and interactions with the reactor coolant system
b2) low pressure phenomena
b3) phenomena related specifically to new components, systems or reactor configurations.

In current generation reactors the thermalhydraulic behaviour of the containment system and of the primary system are studied separately. This is not any more possible in most of the new design concepts; suitable tools must be developed to predict the performance of the integrated system.

A peculiarity common to almost all the innovative reactors is the presence of devices that depressurize the primary loop essentially to allow the exploitation of large amount of liquid at atmospheric pressure and to minimize the risk of high pressure core melt: in this case the phenomena may be similar (or the same) as those reported for present generation reactors (Tab. II) but the range of parameters and the safety relevance can be much different.

Finally, the presence of new systems or components and some geometric peculiarities of innovative reactors require the evaluation of additional scenarios and phenomena.

A list of identified phenomena belonging to subclasses b1, b2 and b3 is given in Tab. III; comments related to few of these are reported below.

Behaviour of large pools of liquid (item 1 in Tab. III)

Large pools may have a very wide spectrum of geometric configurations. Heat transfer in one very limited zone in terms of volume (e.g. by condensing steam or by isolation condenser) does not imply homogeneous or nearly homogeneous temperature in the pool. Three-dimensional convection flows may develop affecting the heat transfer process. Liquid drain from relatively small openings may cause rotation of the fluid and entrainment of the gas phase (vortex formation).

Tracking of non-condensables (item 2 in Tab. III)

The non-condensable gases play much more important role in safety evaluation of the innovative reactors than for the current generation reactors, particularly because of the coupling of the containment with primary system. Flow of the non-condensables within the containment and potential stratification and separation of steam, may be an important factor to consider since it may affect heat transfer within the containment and out of the containment. Also, within the primary system the nitrogen transport from accumulators may affect the coolant distribution and pressure response to the degree that gravity driven safety injection may be impacted.

Non-condensible gases are also an important factor in efficiency of isolation condensers and the condensing heat exchangers.

Thermofluidodynamics and pressure drops in various geometrical configurations (item 5 in Tab. III)

Owing to the lack of pumping power the circulation among the various zones of the system constituted by primary circuit and containment depends upon relatively small driving forces. The presence
PHENOMENA OCCURRING DUE TO INTERACTION BETWEEN PRIMARY SYSTEM AND ENVIRONMENT

1. Behaviour of large pools of liquid:
   - thermal stratification
   - natural/driven convection
   - steam condensation (e.g. chugging, etc.)
   - heat and mass transfer at the upper interface (e.g. evaporation)
   - liquid draining from small openings (steam and gas transport)

2. Tracking of non-condensables (essentially, O2, N2, air):
   - effect on mixture to wall heat transfer coefficient
   - mixing with liquid phase
   - mixing with steam phase
   - stratification in large volumes in at very low velocities

3. Condensation on the containment structures:
   - cooling with condensation in larger structures

4. Behaviour of containment emergency systems (POCI, external air cooling, etc.):
   - interaction with primary cooling loops

5. Thermodynamics and pressure drops in various geometrical configurations:
   - 3-D flow paths around open doors, connection of big pipes with ponds, etc.
   - gas/liquid phase separation at low Re and in laminar flow
   - local pressure drops

PHENOMENA OCCURRING AT ATMOSPHERIC PRESSURE

6. Natural circulation:
   - interaction among parallel circulation loops inside and outside the vessel
   - influence of non-condensables

7. Steam liquid interaction:
   - direct condensation
   - pressure waves due to condensation

8. Gravity driven reflux:
   - heat transfer coefficients
   - pressure rise due to evaporation
   - consideration of a closed loop

9. Liquid temperature stratification:
   - lower plume of vessels
   - downcomer of vessel
   - horizontal/vertical piping

PHENOMENA ORIGINATED BY THE PRESENCE OF KEY COMPONENTS AND SYSTEMS OR SPECIAL REACTOR CONFIGURATIONS

10. Behaviour of density locks:
    - stability of the single interface (temperature and density distribution)
    - interaction between two density locks

11. Behaviour of check valves:
    - opening/closure dynamics
    - partial/total failure

12. Critical and supercritical flow in discharge pipes:
    - shock waves
    - supercritical flow in long pipes
    - behaviour of multiple critical sections

13. Behaviour of isolation Condenser

14. Stratification of boron:
    - interaction between chemical and thermohydraulic problems
    - time delay for the boron to become effective in the core

Tab. III - Relevant thermohydraulic phenomena of interest in the innovative reactors.
PHENOMENA OCCURRING DUE TO THE INTERACTION BETWEEN PRIMARY SYSTEM AND CONTAINMENT

1. Behaviour of large pools of liquid:
   - thermal stratification
   - natural/forced convection
   - steam condensation (e.g. chugging, etc.)
   - heat and mass transfer at the upper interface (e.g. evaporation)
   - liquid draining from small openings (steam and gas transport)

2. Tracking of non-condensibles (essentially, He, Ne, air):  
   - effect on mixture to wall heat transfer coefficient
   - mixing with liquid phase
   - mixing with steam phase
   - stratification in large volumes in at very low velocities

3. Condensation on the containment structures:
   - coupling with conduction in larger structures

4. Behaviour of containment emergency systems (KCCD, external air cooling, etc.):
   - interaction with primary cooling loops

5. Thermofluidynamics and pressure drops in various geometrical configurations:
   - 3-D flow paths around open doors, connection of big pipes with pools, etc.
   - gas/liquid phase separation at low ke and in laminar flow
   - local pressure drops

PHENOMENA OCCURRING AT ASYMMETRIC PRESSURES

6. Natural circulation:
   - interaction among parallel circulation loops inside and outside the vessel
   - influence of non-condensibles

7. Steam liquid interaction:
   - direct condensation
   - pressure waves due to condensation

8. Gravity driven reflood:
   - heat transfer coefficients
   - pressure rise due to evaporation
   - consideration of a closed loop

9. Liquid temperature stratification:
   - lower plumes of vessel
   - downcomer of vessel
   - horizontal/vertical piping

PHENOMENA ORIGINATED BY THE PRESENCE OF NEW COMPONENTS AND SYSTEMS OR SPECIAL TRACTOR CONFIGURATIONS

10. Behaviour of density locks:
    - stability of the single interface (temperature and density distribution)
    - interaction between two density locks

11. Behaviour of check valves:
    - opening/closure dynamics
    - partial/full failure

12. Critical and supercritical flow in discharge pipes:
    - shock waves
    - supercritical flow in long pipes
    - behaviour of multiple critical sections

13. Behaviour of Isolation Condenser

14. Stratification of bunsen:
    - interaction between chemical and thermohydraulic problems
    - time delay for the bunsen to become effective in the core

Tab. III - Relevant thermal-hydraulic phenomena of interest in the innovative reactors.
of obstacles like bends, valves, etc., that have no relevance when pumps are running, can be important for the evolution of the phenomena. Various mechanisms of phase separation at very small Reynolds number may interfere with the establishment of flowrates.

**Natural Circulation (item 6 in Tab. III)**

Natural circulation is a complex phenomenon depending upon some other phenomena mentioned in Tab. III. The interaction between multiple parallel flow paths may be critical especially in long lasting transients.

**Gravity driven reflood (item 8 in Tab. III)**

Reflood has been widely considered in safety studies related to the present generation reactors. Additional aspects of interest in the innovative reactors are the presence of feedback between the velocity of the quench front, the pressure rise due to vaporization (at the quench front) and the condensation of steam possibly in the same tank supplying liquid for reflood.

**Behaviour of density locks (item 10 in Tab. III)**

The stability of the interface of the density locks, especially when two density locks are present, appears a critical aspect; possible long term variations of static head in the pool (e.g. due to stratification or heating) may change the interface position in each density lock and the stability characteristics.

**Behaviour of check valves (item 11 in Tab. III)**

Check valves connect the primary circuit with very large volumes through large pipes. Conditions in the piping with check valves may arise that cause rapid condensation on one side resulting in slam close of the valve. The small driving forces may not reopen the valves again. The failure to open may prevent the coolant to flow into the primary circuit; the failure to close may cause fast draining of primary circuit; opening and closure cycles cause critical oscillations in the flow rates.

**4. OVERVIEW OF EXPERIMENTAL ACTIVITIES IN PROGRESS**

Experimental activities carried out, in progress or planned for the three considered reactor types have been reviewed. Few results available from the literature are summarized in the following three sections. Only preliminary conclusions can be drawn about the consistency between the researches objectives and the relevant phenomena listed in Tab. III.

**4.1 SBWR experimental activities**

Examples of experimental activities related to SBWR are documented in refs. /10/ to /15/.

**Toshiba activities**

The main purpose of the facility constructed and operated by Toshiba is the real time simulation of long term transient occurring in the SBWR/10/, /11/. The test rig (sketch in Fig. 4) has a volume scaling ratio of 1/400, a height scaling ratio of 1/1, operating pressure of around 0.3 MPa.

These tests were designed to evaluate the condensation heat transfer in the PCCS condensers in presence of non-condensables. Also these tests provided data on non-condensable purging. The PCCS condensers are connected to the containment drywell and during steam release to the drywell (due to steam line break or action of the
automatic depressurization system) steam and containment nitrogen flows into the condensers. Presence of noncondensable gas reduces significantly efficiency of the condensers. The design calls for a purging system that removes the nitrogen from the condenser to the suppression pool.

The degradation of the heat transfer coefficient as a function of nitrogen concentration is shown in Fig. 5. Local and overall values of heat transfer coefficients are considered: the differences between the two increase when nitrogen concentration increases and is connected with the larger area needed for the condensation. In the same figure, the prediction obtained by the Sparrow model[10] is also reported. As a main result, it was found that IC had sufficient heat removal capacity even in presence of nitrogen and that the degradation in heat transfer coefficient for forced condensation was much milder than the values foreseen for stagnant condensation.

A wide data base has been measured in the above facility in relation to the overall system response in case of an accident. The process is as follows in case of a steam line break: the steam coming from the vessel directly reaches the drywell where it mixes with nitrogen. The steam-nitrogen mixture reaches the IC where steam is condensed (going back to the vessel) and nitrogen causes pressure increase up to clear special vent lines connecting IC with the suppression pool. At this time nitrogen vents to the suppression pool.
chamber increasing again the heat removal capability of the IC. Suppression pool and drywell are also connected by direct nitrogen vent lines and by vacuum breakers.

Vent submergence of nitrogen vent lines is such to maintain the flow from drywell to IC to suppression pool up to limit values of nitrogen concentration; vacuum breakers opening cause direct flow from suppression pool to drywell. Experimental data related to the operation of the whole system essentially confirm the adequacy of the nitrogen venting mechanism\(^{111}\) and suggest that there is the possibility to optimize the system response by varying the relative submergences inside the suppression pool of the lines connecting this zone with IC and with the drywell.

**Activities at PSI and SIET**

Construction of a large facility (1/24) to evaluate the SBWR containment behavior is planned at PSI\(^{112}\). This facility will provide for scaled suppression pool, drywell, and IC and FCCS condensers pools. Interactions between individual compartments of the containment and phenomena such as steam noncondensable stratification will be studied additionally to the FCCS performance. Also aerosol pool scrubbing phenomena will be investigated.

Two isolation condensers are now in the final stage of construction at SIET\(^{113}\), one at low pressure for testing the passive containment cooling and the other one at high pressure for measuring the heat removal capability from the primary circuit. The available power is up to 20 MW and design pressure is 10 MPa for the facility simulating primary circuit behaviour.

**Use of PIPER-ONE facility**

Piper-one simulates a BWR-6 with volume and height scaling ratios of 1/2200 and 1/1 respectively and is installed at DCMN of University of Pisa. The electrical power supplied to the core rods is, roughly 25% of the ideally scaled value and is sufficient to simulate long lasting transients with scram.

The facility has been modified two times with the introduction of the Gravity Driven Cooling System GDCS\(^{114}\) and the IC\(^{115}\), respectively. In both cases the vessel configuration remained the same simulating the BWR-6; so the experimental data can be used for the
evaluation of the qualitative performance of the two added systems (and obviously for code validation purposes).

Three experiments were performed in the GDCS configuration (Fig. 6) and demonstrated a huge influence of the boundary conditions and of pressure drops upon the involved phenomena: in test PO-SD-6A (break and GDCS pool connected to the atmosphere) the pressure rise at the quench front prevented core reflood (Fig. 7); in test PO-SD-6C (break discharging into GDCS pool), GDCS was effective in quenching the core (Fig. 8).

Fig. 6 - Sketch of PIPER-ONE (GDCS configuration).

In the IC configuration a pipe was added at the top of the main vessel carrying the steam into a heat exchanger placed inside a pool at atmospheric pressure. The bottom line of the heat exchanger primary side was connected with the lower plenum of the facility. Tests have been performed by varying the core power (up to 15% of the decay value) and system pressure (up to 5 MPa). The evaluation of
Fig. 7 - Rod surface temperature trends and core power during PIPER-ONE GCOS experiment (pool at atmospheric pressure).

Fig. 8 - Rod surface temperature trends and core power during PIPER-ONE GCOS experiment (pool connected with break line).

Experimental data is in progress. As a preliminary result, strong temperature stratification was observed inside the IC pool, where the heat exchanger was purposely put in the upper part. The pool was boiling at the top with temperatures as low as 293 K in the bottom.

4.2 AP-600 experimental activities

Westinghouse conducts a comprehensive testing program that covers all the new passive safety features. Only limited information
about this program was published in the open literature. References /16/ and /17/ provide some overview of planned and performed AP600 testing activities.

A prototypic representation of a complete passive residual heat removal (PRHR) loop (Fig. 9) has been in operation by Westinghouse/16/. A series of tests at different temperatures in single phase flow demonstrated the suitability of the component in transferring to the IRWST adequate amount of thermal power. Typical data of heat removed per tube as a function of tube flow rate are shown in Fig. 10; the parameter on the right is the entrance temperature of primary fluid. As expected, higher primary fluid temperature and higher mass flow rates result in higher heat transfer. Another area of interest in the research deals with the problem of stratification and natural convection inside the pool. Roughly, 40 K have been found as the maximum temperature difference inside the pool with the largest part of the fluid volume at boiling conditions. Installation of a special baffle inside the pool and the proper position of the heat exchanger led to the optimization of the pool heat sink capacity.

Plans for modifying the Spes facility available at SIET (PWR simulator with 1/427 volume and power scaling factor) have been defined into detail/17/. The new facility (called Spes-2) will simulate AP-600, with a volume scaling ratio given by 1/395. Now, design calculations have been performed to compute the predicted scenarios in AP-600 and in Spes-2. This led to a modification of volume scaling ratio of the core make-up tanks to better reproduce the predicted plant behaviour.

![Fig. 9 - Sketch of Westinghouse PRHR test facility.](image-url)
4.3 PIUS experimental activities

The PIUS concept apparently contains more innovation with respect to the previous reactors; so more researches can be expected. Example of experiment related to PIUS can be found in refs. 18 to 21.

Two facilities have been constructed in Sweden and in Japan and are shown in Figs. 11 and 12, respectively. These have essentially the same objectives of demonstrating the possibility of the reactor concept and to validate computer models.

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**Fig. 10** - Westinghouse PHWR Heat Exchanger Test: Heat Transfer Versus PHWR Tube Flow (Per Tube Basis; Saturated Tank Conditions).

**Fig. 11** - Sketch of the ANS ATLE test loop.
The reactor simulator in the Atle loop has the same height as
the reactor in the real plant; the overall volume scale is 1/308. The
Japanese simulator\textsuperscript{1199} is a low pressure scaled loop and comprises the
various zones of the plant. Several transient scenarios have been
measured in both test rigs.

Typical transient data from Atle are shown in Figs. 13 and 14.
Following a loss of heat sink, hot water arrives in the downcomer of
the facility causing temperature increase in the core region (Fig.
13). The presence of hot water affects the pressure balance causing a
net inflow into the core from the cold water tank through the lower
density lock. The inflow of cold (borated) water is intermittent until
the pump controller restores the interface in the lower density lock.
Fig. 14 - ATLE rig: water flow through the lower density lock following a partial loss of heat sink (the flowmeter shows the absolute value of the flow).

A similar loss of feedwater transient was considered in the Japanese facility assuming a different control of the primary circulation pumps. Relevant results are shown in Figs. 15 and 16. In this case the hot water inlet in primary circuit is balanced by the pump velocity increase up to the limit velocity (50 Hz) when pump trip occurs (after 2000 s in Fig. 15). This causes stable flow of cold water into the main loop through the lower density lock.

Fig. 15 - Japanese rig: system response in case of loss of feedwater.

The experimental research by Pind and Fredell was focused on the evaluation of the transport processes in a single density lock. Mechanisms identified were the boron mixing and diffusion at molecular level and the instability of the interface caused by natural convection flow in the cold pool. The first problem makes necessary a boron purification loop in the PUS; the second problem confirmed the occurrence of "sloshing" oscillations, requiring full scale testing.
5. SUITABILITY OF COMPUTER CODES IN APPLICATIONS TO INNOVATIVE REACTORS

A large number of calculations have been performed with LWR system codes (Relap5 Trac, Cathare, Thyde) and with specially developed codes (e.g. Rigel, Trip, Fumo, etc.).

Examples of analyses documented in the literature can be found in refs. /22/ to /27/ related to the first group of codes and in refs. /28/ to /30/ related to the second group. Additional code applications can also be found in the previously listed references.

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System codes are widely used at University of Pisa (e.g. /31/, /32/); Relap5/mod2 code deficiencies have been found in the application to transient analysis in both SBWR and AP-600 plants. Relevant results from the application to large break and interfacing LOCA (ILOCA) analyses related to the last reactor are given below/37/.

Position of sparger in the IRWST

The reference ILOCA calculation (Fig. 17) was performed assuming that the sparger of the depressuration system in the IRWST pool is located near the top of the volume occupied by the liquid. In this case only partial condensation of the steam discharged from the primary system is observed; the remaining part of the steam contributes to the pressurization of the pool volume, allowing the drainage of the liquid from the pool towards the core. A second calculation was carried out assuming that the sparger is located in the bottom of the pool. In this case, a nearly complete steam condensation prevents the pool liquid from discharging and leads to extended core dryout (Fig. 18).

Effect of liquid temperature in the pool

Again with reference to the ILOCA in the AP-600 plant, while the reference calculation was done assuming an initial temperature of the liquid in the Core Make up Tanks equal to 373 K (to prevent the stop
of the calculation owing to numerical problems), a second calculation was run considering a lower (323 K) liquid temperature in the Core Make up Tanks. In this case the condensation in the connection zone between the CMTs lines and the vessel creates a steam upward flow in the lines that prevents liquid drainage and causes dryout early in the transient (Fig. 19).

**Effect of nodalization of the CMT discharge lines**

The line connecting the CMT to the vessel presents a complex layout in the vertical planes. Different approaches in modelling the line result in quite different predictions of CMT discharge. The CMT levels in the reference case for LBLOCA, are compared with those
obtained in the sensitivity study in Fig. 20**. In the reference case the fairly coarse nodalization of the discharge lines modifies the pressure drop situation in the plant causing flow from the cold leg to one of the CMT, that does not experience any level decrease despite the liquid delivery to the vessel. The nearly constant level situation in one tank prevents the activation of the depressurization system. The more detailed nodalization used in the sensitivity calculation allows the complete discharge of both CMT's.

** The steps observable in the curves for the reference case occur when the level crosses the volume boundary and constitute a nodalization effect.
automatic depressurization system) steam and containment nitrogen flows into the condensers. Presence of noncondensable gas reduces significantly efficiency of the condensers. The design calls for a purging system that removes the nitrogen from the condenser to the suppression pool.

The degradation of the heat transfer coefficient as a function of nitrogen concentration is shown in Fig. 5. Local and overall values of heat transfer coefficients are considered: the differences between the two increase when nitrogen concentration increases and is connected with the larger area needed for the condensation. In the same figure, the prediction obtained by the Sparrow model\textsuperscript{1}\textsuperscript{e} is also reported. As a main result, it was found that IC had sufficient heat removal capacity even in presence of nitrogen and that the degradation in heat transfer coefficient for forced condensation was much milder than the values foreseen for stagnant condensation.

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![Diagram](image)

Fig. 13 - ATLE rig: water temperature in the reactor core after a partial loss of heat sink.
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<sup>→</sup> The steps observable in the curves for the reference case occur when the level crosses the volume boundary and constitute a nodalization effect.
5.2 Specific deficiencies of codes

Deficiencies and capabilities of system codes are widely discussed by the international community. Findings relevant to the new generation reactors are discussed hereafter; these are the direct outcome of the experience acquired in the use of Relap5/mod2, but can be extended to other advanced system codes.

In connection with the use of codes in predicting transient scenarios in the innovative reactors, essentially, two phases in the event time sequences can be distinguished:

a) primary system pressure greater than about 0.5 MPa;

b) subsequent period including a tight interaction between the primary loop and the containment system and the long term behaviour of the passive safety systems.

The physical situations foreseeable in the first period are characterized by parameter ranges for which a very wide data base (experiments and code calculations) already exists. However, this data base does not cover all of the new design features. Some of those will operate in ranges for which models were not developed or properly assessed. Example of such are the CMTs in the AP600 design. The large thermal gradient in the CMTs causes very high condensation rate. One can expect that during CMT draining a layer of saturated liquid will form over the subcooled water significantly reducing the condensation. Model for such thermal stratification does exist in present codes. Code capabilities and limits can be retained the same as applicable for the present generation reactors and will not be discussed furtherly in this paper.

On the other hand, most of the phenomena foreseeable in the phase b) should be considered outside the qualification boundary of the codes. Some of them are also outside the validity limits of the correlations and of the numerical structure of the codes.

For a systematic evaluation of the codes limits and capabilities, all the phenomena listed in Tab. III should be considered in the assessment process. Such results are not available for the time being, and only few generic aspects are emphasized below.

List of deficiencies

1. At pressure close to the atmospheric value, very large oscillations may occur in the physical quantities. In some cases this is the results of the applied numerical scheme and of discontinuities of the functions simulating the water properties with main concern to the derivatives. As a consequence of this, very small time steps must be used (this is not practical for long lasting transients) and frequent interruption of the calculation may occur.

2. The simulation of the fluid behaviour downstream of a critical section (supercritical flows) is not allowed in any geometric situation. Steam superheating may be important in the condensation process inside pools.

3. The transition between critical flow model and the ordinary differential equation model to calculate flowrate leads to oscillations in the calculation.
4. The possible occurrence of multiple critical sections in a complex piping appears to be outside the prediction capabilities.

5. The stratification of temperatures in pools or tanks is not well calculated by the present system codes: even the natural convection circulations establishing when a heat source and sink are present are not calculated. This may result in wrong predictions of gravity head, and errors in mass flow rates and heat exchange coefficients.

6. The natural circulation occurring in several parallel loops (the common element to almost all of these being the reactor core) largely depends upon local loss coefficients in complex three-dimensional geometries. These have much more importance when pumping power is lacking and must be supplied as input by the user. Integral system data will be required to assess the capability of the codes to predict the natural circulation in complex systems.

7. The capability to track non-condensable gases in the whole spectrum of foreseeable conditions of temperature, velocity and gas fraction does not appear adequate. Especially the steam-gas separation process is not considered.

8. The evaluation of both direct (e.g. at ECC port, inside large pools, etc.) and indirect condensation (e.g. inside IC tubes) does not appear adequate, particularly in presence of noncondensible gases.

9. The zero dimensional neutronic kinetics is not suitable for simulating 3D behaviour of large cores (this important limitation also applies for current reactors).

10. The codes numerical solution scheme appears not adequate to handle transients lasting several hours.

As a consequence, especially of items 5, 6 and 8 the codes are not able to simulate the integrate behaviour of primary system and containment.

6. CONCLUSIONS

An overview has been given in this paper of relevant thermal-hydraulic aspect applicable to the new generation reactors. The thermal-hydraulics of evolutionary reactors do not imply the occurrence of new accident scenarios compared to current generation reactors. The same conclusion does not apply to the innovative reactors.

SBRW, AP-600 and PIUS have been specifically considered summarizing some results of experimental studies and of code applications. The main outcomes are the classification of a series of phenomena important for the evaluation of transient performance of the mentioned reactors and the identification of specific code limits.

Relevant differences between scenarios in current and innovative type of reactors can be added to two facts:
- evolution of the largest part of scenarios at low pressure (near the atmospheric value) in the innovative reactors;
- tight interaction between primary system and containment and the passive safety systems also implying the occurrence of several parallel natural circulation loops each one including (possibly) pools where direct steam condensation takes place and the transport of large amounts of non-condensible gas.

The comparison between the foreseeable new plant phenomena and the objectives of the documented experimental researches, demonstrate that some critical issues have been considered, but the spectrum of potentially interesting thermal-hydraulic aspects is far larger than the number of phenomena taken into account. Furthermore the available data base must be considered still preliminary and not fully exploited. New researches can be planned on this basis.

The application of currently available system codes to off-normal conditions typical of the innovative reactors allowed to distinguish two periods that are separated by a pressure boundary set at about 0.5 MPa: when the primary system pressure is above that value, the code suitability and applicability is essentially the same as in current generation codes; at pressure below that value, the occurrence of new phenomena and intrinsic code limitations (of low interest for current reactors) prevent, in the general case, the possibility of a reliable simulation of plant scenarios. A list of 10 specific areas in the codes that need improvement has been produced.

Finally innovative reactors contain technological features that are common to the present generation reactors. From a thermal-hydraulic point of view, PIUS is characterized by the largest innovation: the safety is not dependent upon added external circuits or components but is intrinsic to the reactor concept. As such it requires larger investigation to prove its suitability.

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THERMALHYDRAULIC SAFETY ANALYSIS
OF THE CANDU REACTOR

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(5) New Brunswick Power, Lepreau, New Brunswick EOG 2H0

ABSTRACT

The thermalhydraulic analysis requirements for the safety and licensing of the
CANDU® reactor are outlined. The unique features of the CANDU design are first
described, and the specialized analysis requirements for the reactor are identified.
Thermalhydraulic codes used to perform the analysis are presented and the
experimental test programs used to validate the codes are described. The paper
concludes with future plans for the experimental test programs, code development,
and code validation.

1. INTRODUCTION

The development of methods for CANDU safety analysis has proceeded in parallel with,
and independently from the development of safety analysis methods for light-water
reactors. The unique CANDU design with horizontal pressure tubes and a heavy water
moderator has required the development of corresponding unique analysis methods and
computerized simulation models. Although these models have elements in common with
those used in light-water reactor analysis, specialized code development has been needed.
This development has focused on analysis related to horizontal flow regimes in parallel
piping channels and on modelling aspects related to demonstrating the effectiveness of
the moderator system as an inherent heat sink. The complete accident analysis code suite
includes models for reactor physics, thermalhydraulics, fuel, fuel channels, containment
and, finally, dispersion and dose.

The validation of these CANDU analysis tools has required a supporting experimental pro-
gram. Reactor safety and licensing needs have advanced our understanding of thermal-
hydraulics, fuel channel behaviour in accidents, hydrogen generation and combustion and
the behaviour of radioactive materials in containment. Experimental facilities have been
established in parallel with the development of analytic methods. The periodic updating of
safety reports by licensees provides the vehicle for incorporating advances in analytic and
experimental information into the safety analysis.

This paper provides an overview of the analysis methodology, with an emphasis on the
thermalhydraulic aspects of the safety analysis. A brief description of the CANDU reactor
design is presented to put the analysis in context.

* CANDU (CANada Deuterium Uranium) is a registered trademark of AECL.
2. CANDU REACTOR

Figure 1 shows a simplified schematic of the major process systems common to all CANDU designs.

2.1 MODERATOR SYSTEM

A noted feature of the CANDU reactor is the large horizontal cylindrical calandria vessel containing heavy water which serves as the neutron moderator. Several hundred Zircaloy tubes, called calandria tubes, are arranged within and parallel to the cylindrical shell. They are fastened with leak-tight joints to tubesheets at the ends of the calandria. The heavy water moderator is operated at essentially atmospheric pressure and the temperature is maintained well below the boiling temperature of water at the operating pressure.

2.2 REACTOR HEAT TRANSPORT SYSTEM AND FUEL CHANNELS

The reactor heat transport system is arranged in either one or two figure-of-eight loops in which large-diameter piping connects major components, such as the steam generators and pumps, to the reactor header pipes. Approximately 100 distributed, small-diameter feeder pipes are connected from these large-diameter headers to the end fittings of the horizontal fuel channels. The end fittings are special components designed to accommodate on-power refuelling. Figure 2 shows a simplified schematic of a typical two-loop CANDU heat transport system design.

The fuel channel assemblies consist of Zr-2.5% Nb alloy pressure tubes connected by rolled joints to the body of the end fittings at either end of the reactor. The pressure tubes are contained within thin-wall Zircaloy calandria tubes, and the annulus between the two tubes contains slowly circulating carbon dioxide gas, which helps to insulate the hot pressure tube from the cold moderator fluid surrounding the calandria tube. The two tubes are separated by a series of coiled Zircaloy springs, called garter springs, placed at a number of locations along the pressure tube.

Each pressure tube contains either 12 or 13 fuel bundles, depending on the reactor design. These fuel bundles (see Figure 3) are approximately 0.5 m long and contain 28 or 37 fuel elements configured in arrays of concentric rings of elements welded to the end plates. The fuel elements are made of sintered pellets of natural uranium dioxide contained with thin-walled Zircaloy sheathing tubes designed to collapse onto the pellets at operating pressures. As mentioned earlier, a unique feature of CANDU reactors is on-power fuelling, where irradiated bundles are discharged at one end of a channel and new bundles are inserted at the other end.

2.3 SAFETY SYSTEMS

Safety systems include the reactor shutdown systems, the emergency core cooling system and the containment.
2.3.1 Shutdown Systems

Two independent reactor shutdown systems are provided. Additional negative reactivity can enter the reactor core through tubes installed into the reactor from the top and sides (see Figure 4). Shutdown System 1 employs a large number of distributed shut-off rod units that can be driven rapidly from above into the core by spring-assisted gravity action. Shutdown System 2 relies on the gas-driven injection of heavy water containing a dissolved absorber ("poison") into the moderator.

2.3.2 Emergency Core Cooling and Containment Systems

CANDU reactors, in common with other reactors, are fitted with emergency core cooling systems and containment systems. The emergency cooling systems are designed to operate over the complete range of postulated loss-of-coolant accidents, and can operate at high and low pressures.

Containment systems fitted to CANDU reactors vary from multi-unit systems with water spray and vacuum building pressure suppression to single-unit systems that rely on the reinforced concrete containment vessel without pressure suppression.

3. CANDU SAFETY ANALYSIS

The purpose of the safety analysis is to demonstrate that the risk to the public from the operation of the reactor is acceptably low. Canadian utilities operate within a set of regulations established by the Canadian Atomic Energy Control Board (AECB). The utilities submit their analyses and operational practices to the AECB for review. The submissions provide details of the design and safety assessments, which the AECB reviews before granting an operating licence. The AECB criteria is for the safety analysis to demonstrate that prescribed limits will not be exceeded following a reactor accident.

3.1 SAFETY DESIGN PHILOSOPHY

The Canadian nuclear safety philosophy, which is similar to that adopted in other countries, is based upon the principle of "defence-in-depth." This is established by designing, constructing and operating nuclear power plants in a manner such that radioactive materials are contained within a succession of physical barriers. To assure acceptably high levels of safety, the CANDU design employs physical and functional separation between process and safety systems and, to the greatest extent achievable, diversity and independence between systems.

Reliability objectives are accomplished by providing independent diversified systems (either safety systems or combinations of process and safety systems) to fulfill each of the safety functions. Every safety system and certain important process systems are provided with redundancy in the active components. Safety systems are also designed to be readily testable during operation to demonstrate an unavailability of less than $10^{-1}$. It is also standard practice to assess the expected frequency of each postulated initiating event and the probability of impairment of the safety functions required to cope with it. The purpose of these probabilistic safety assessments is to verify that the expected frequency of an accident with large releases of radioactivity is acceptably low.
3.2 ACCIDENT ANALYSIS PHILOSOPHY

The objective of accident analysis is to demonstrate that the plant design will limit radioactivity releases to levels at or below those prescribed by regulators that are designed to protect the public. The analysis considers two categories of events:

1. single failures, i.e., the failure of a process system requiring actuation of one or more of the safety systems, and

2. dual failures, i.e., the failure of a process system combined with the unavailability or impairment of any one of the safety systems.

A loss-of-coolant accident and a loss of reactor control are typical single failures, while a loss-of-coolant accident combined with the unavailability of the emergency core cooling system is an example of a dual failure.

The set of single- and dual-failure accidents analyzed for a typical CANDU generating station is listed in the accident analysis matrix in Table 1.

The separation of process and safety functions through separate systems is intended to ensure independent functioning of safety systems should a process system fail. Protection against common events is achieved via the two-group concept. Process and safety systems are separated into two groups, each of which can provide the three fundamental reactor safety functions (shutdown, fuel cooling and plant monitoring) separately and independently.

3.3 CANDU ACCIDENT ANALYSIS

The major features of the CANDU system that affect accident analyses include:

- a reactor pressure boundary that consists of many small-diameter horizontal thin-wall pressure tubes,

- a separate low-pressure and low-temperature heavy water moderator with its own circulation and cooling system, and

- on-power fuelling with a consequent need to open reactor channels and transfer fuel during operation.

These features imply the consideration of horizontal-flow thermalhydraulics and fuelling machine operation in accident analyses. The nature of the materials used to build the reactor and their geometric arrangement results in an increase in reactivity and power as a result of void formation following the initiation of loss of coolant. This inherent feature has resulted in a design that incorporates two independent quick-acting shutdown systems. Safety analyses must take into account the initiation time of these systems during the power increase resulting from steam formation in the core following a loss-of-coolant accident.

The moderator system provides an inherent heat sink that can be counted on to remove decay heat if the emergency cooling system is unavailable.
3.3.1 Loss-of-Coolant Analysis

The analysis of the consequences of loss-of-coolant accidents in a CANDU heat transport system forms a major part of accident analysis, which involves an interactive approach based on the following analysis disciplines:

- reactor physics (neutron kinetics),
- heat transport system thermalhydraulics,
- fuel response,
- fuel channel response (pressure tubes and calandria tubes),
- moderator response,
- containment thermalhydraulics,
- radionuclide behaviour, and
- atmospheric dispersion and public dose.

Sections 4 and 5 describe the activities associated with model development and validation in detail.

4. ANALYSIS CODES

This section focuses on the codes used to analyse the CANDU primary (and secondary) heat transport systems. To date, a significant portion of the thermalhydraulic analysis has been performed by homogeneous EVET (liquid and vapour flows have equal temperature and velocity) codes supplemented by correlations to account for slip effects in horizontal pipes and drift effects in vertical pipes. Two such codes are SOPHT [1] and FIREBIRD [2]. These codes have been used to analyse flow conditions consistent with the homogeneous assumption. In areas where non-equilibrium effects are expected to dominate (e.g., low flows), specialized codes are used or a bounding analysis is performed.

During the last few years, an increasing portion of this analysis has been performed by two-fluid codes (liquid and vapour phases may have different velocities, temperatures and pressures). CATHENA and TUF are two two-fluid one-dimensional (1-D) codes currently being used for the thermalhydraulic safety analysis of the CANDU in Canada.

Under low-flow conditions in the horizontal CANDU fuel channel, thermalhydraulic conditions in the cross section of the channel will vary. The 3-D code, ASSERT, has been developed to predict the sub-channel flows (between the fuel pins) for these conditions under both normal and postulated accident conditions.

4.1 CATHENA

CATHENA, developed by AECL Research, has evolved with the objective of providing a high degree of flexibility in modelling thermalhydraulic systems. Although developed primarily for the analysis of CANDU nuclear reactors, the code has been successfully applied in the analysis and design of experimental test programs. CATHENA is also being used in support of the design, safety and licensing of research reactors developed by AECL (e.g., MAPLE-X10 [3]).

The CATHENA code uses a non-equilibrium, two-fluid thermalhydraulic model to describe fluid flow. Conservation equations for mass, momentum and energy are solved for each phase (liquid and vapour), resulting in a 6-equation model. Also, up to four noncondensible gases may be represented as part of the vapour phase, yielding a 7- to 10-equation model.
Interphase mass, momentum and energy transfer are flow-regime-dependent, and are calculated using constitutive relationships obtained from the literature or are derived from single-effects experiments.

The numerical solution technique used to solve the conservation equations is a staggered-mesh, semi-implicit, finite-difference method. The dependent variables defining the state of a node or cell are pressure, void fraction, and phase enthalpies. If noncondensable gas(es) are present, the noncondensable fractions are also dependent variables. For connections between nodes (called links), the dependent variables are the velocities of the gas and liquid phases. Conservation of mass is achieved using a truncation error correction technique similar to that used in RELAP5/Mod2 [4].

A one-step finite-difference numerical solution scheme has been adopted that is not transit-time-limited. The resulting set of equations is not reduced to a pressure- or flow-field approach. A time-step controller implemented in CATHENA automatically selects the next time step at each finite-difference time step. This is accomplished by monitoring changes in the dependent variables, the selected derived variables, and the truncation error. If the maximum change is below a prescribed value, the time step is increased; if the change is above a maximum prescribed value, it is decreased. The user may alter the default selection criteria through input data and thus check the temporal convergence of a given simulation.

Heat transfer from metal surfaces is handled by an extensive wall-heat-transfer package. A set of flow-regime-dependent constitutive relations specifies the energy transfer between the fluid and the pipe wall and/or the fuel element surfaces. A variational finite-element method is used to model the heat transfer by conduction within the piping and fuel in the radial direction, and the heat transfer can also be modeled in the circumferential direction. The radiative heat transfer and the zirconium/steam reaction rates can also be calculated. The ability to calculate the heat transfer from individual groups of pins in a fuel bundle subjected to stratified flow is built into this package. Under these conditions, the top pins in a bundle are exposed to steam, while the bottom pins are exposed to liquid.

Component models that describe the behavior of pumps, valves, pressurizers, steam separators, and discharge through breaks are available to complete the idealizations of the reactor systems. As well, reactor control systems may be described through the input data. Reference 5 provides a more complete description of the CATHENA code, and References 6 and 7 describe the validation process.

4.2 TUF

The TUF code [8] is the best-estimate thermal-hydraulic system code developed for safety analyses of CANDU reactors operated by Ontario Hydro. It contains modeling of network thermal-hydraulics, heat conduction, neutron kinetics, special components and reactor control systems. The objective of TUF is to provide a two-fluid tool that will enhance the capability of Ontario Hydro to analyze postulated reactor accidents and to assist in reactor design and operation.

Both one- and two-fluid thermal-hydraulic models are included in the code. The conservation equations for the mixture and one particular phase (either vapour or liquid) are solved in the TUF two-fluid model. Consequently, the physics involved in the differences between one- and two-fluid results can be identified.

An additional set of differential equations is used in the two-fluid model to describe the relationships between phase velocities and temperatures, and additional constitutive correlations
are required for the interfacial transfers. The latter correlations are flow-regime-dependent and appropriate flow regime maps have been incorporated in the code. Interfacial area concentration plays a predominant role in interfacial mass, momentum and energy transfers. The entrained bubbles in the slug and stratified flow regimes and entrained droplets in the annular and separated flow regimes have been considered in the calculation of interfacial area. The pressure-field approach is used in TUF to solve the thermalhydraulic equations whereby a simple two-step method, a combination of an explicit and an implicit formula, is used. It has proven to be the simplest and most efficient numerical technique for the one- and two-fluid models.

The thermal non-equilibrium model uses three components in the interphase mass and energy transfers. A boiling parameter is introduced in the pressure transient term. This parameter controls the pressure wave propagation velocity, and hence the critical flow rate. In the non-homogeneous momentum model, the covariant or flow distribution, virtual mass force and interfacial pressure terms play the most significant role in thermalhydraulics (for example, in flow regime transition and flooding).

TUF assessment and testing has generally proceeded in two stages: developmental assessment and plant testing. The developmental assessment involves fundamental physics of governing equations, analytical comparisons, separated effects and system responses. The commissioning tests and plant measurements during abnormal operational conditions provide data for the full-scale assessment of the TUF code.

A pressurizer insurce experiment was conducted at the Nuclear Power Demonstration Reactor in 1985 by Ontario Hydro [9], and is presented as an example of the assessment of the TUF code. The volume of the pressurizer vessel is 5.66 m³. The upper level is 3.9 m and the vessel diameter is 1.27 m. The initial fluid temperature is 280°C. The case of an insurce experiment (No. 11) was simulated where the insurce liquid temperature is 155°C and the insurce flow rate starts at time 35 s and stops at time 150 s.

Three different thermal models (adiabatic, equilibrium and non-equilibrium) were applied. In the thermal non-equilibrium model, the interfacial heat transfer coefficient for the liquid phase was calculated to be about 0.25 to 0.35 kW/(m²·C), which agrees with other investigations. Figure 5 compares the TUF simulation and the experimental data for the pressure transient. The agreement is excellent for the thermal non-equilibrium model, but a large pressure drop is predicted by the thermal equilibrium EVET model.

4.3 ASSERT

The ASSERT subchannel code [10] was developed to calculate the flow and phase distribution within the subchannels of CANDU bundles, which are horizontal. Unlike conventional subchannel codes such as COBRA [11], which are designed primarily to model flow in vertical fuel bundles and use a homogenous mixture model of two-phase flow, ASSERT uses a drift-flux model. This permits the phases to have unequal velocities, and includes gravity terms to make it possible to analyze separation tendencies that may occur in horizontal flow. Parallel phenomenological investigation is required to ensure that the computer code incorporates the mechanisms necessary to simulate experimentally observed trends. The ASSERT development program, therefore, contains a number of coordinated experimental and analytical projects, each providing information essential for the central project. An example of this process is described in Reference 12.

The numerical solution over the bundle cross-section at each axial position is split into two parts. The first solves the energy and state equations, using block iteration to calculate the
mixture and phasic enthalpies for all subchannels, with current flow estimates used as parameters. Once the inner iteration on the energy equation solution converges, the second part calculates flows and pressure gradients at that axial position. This is done by direct matrix solution of the cross-flow equations, from which it is possible to calculate the axial flows and pressure gradients. The channel is successively swept from the inlet to the exit until convergence is achieved.

The current numerical method in the production version of ASSERT is restricted to positive axial flows. An improved algorithm based on pressure-velocity methods has been implemented in the development version and has been used successfully for recirculating flows.

5. EXPERIMENTAL PROGRAMS

5.1 INTRODUCTION

Experimental test programs have been undertaken to improve our understanding of relevant thermalhydraulic phenomena in the CANDU and to generate a data base to validate analysis codes. Most programs are currently performed under the direction of the CANDU Owners Group (COG). The overall program is structured in three parts:

1. fundamental experiments,
2. component experiments, and
3. integral experiments.

The present discussion is limited to the validation of the 1-D codes; Reference 12 describes the ASSERT validation in detail.

5.2 SINGLE-EFFECTS EXPERIMENTS

Single-effects experiments focus on the requirement to understand basic two-phase flow phenomena. Examples include flow regimes and their resulting constitutive relations [13], and countercurrent flow flooding phenomena [14]. Generally, these experiments are used in the initial stages of the code validation process.

5.3 COMPONENT EXPERIMENTS

Component experiments are designed to investigate the thermalhydraulic phenomena occurring in components of the CANDU heat-transport systems under upset or loss-of-coolant conditions. Programs have been put in place to characterize the following CANDU components in the primary and secondary heat transport systems:

- feeder/channel assembly,
- header,
- steam generator (blowdown),
- pump, and
- CANDU end-fitting.

It is beyond the scope of this paper to describe all these programs, and only selected key programs are summarized.
5.3.1 Cold-Water Injection Test (CWIT) Facility

The CWIT facility [15], located at Stern Laboratories Inc., Canada, is a full-scale simulation of a CANDU channel-feeder system. Figure 6 shows a schematic diagram of the test facility, which consists of two parallel horizontal channels, two headers, inlet and outlet feeders, two break simulation devices, a blowdown tank, a water injection system, and various measurement and control systems.

Each test channel assembly contains a 6-m electrically heated 37-element fuel string to simulate CANDU fuel at decay power levels. Each test channel assembly is housed in a CANDU-typical pressure tube. End-fitting flow simulators are installed at both ends of the channel and are connected to headers located either 5 or 10 m above the channel by sections of vertical and horizontal feeder piping.

Experiments performed in this facility investigate the CANDU-type feeder and channel refilling and rewetting processes under the emergency coolant injection conditions in a postulated loss-of-coolant accident. As well, fuel cooling in the absence of forced flow is studied.

An example of results obtained from this facility is presented in Figure 7. For this experiment, the objective was to investigate the refilling behaviour of the emergency coolant injection system in the hot (superheated) feeder system. The quench and refill times at various locations can be determined from the thermocouples mounted on the outer surfaces of the feeder piping (refilling is considered to occur when the feeder surface temperature drops below the saturation temperature). Figure 7 shows one location. The quench "front" can be inferred from a number of these measurements along the feeder length.

5.3.2 Large-Scale Header (LASH) Facility

The LASH Facility [16] (see Figure 8) is also located at Stern Laboratories. The test facility consists of an inlet and outlet horizontal header connected by 30 feeders. The inlet and outlet headers are CANDU-typical in diameter, but are half-length with an internal diameter of 0.325 m and a length of 4.2 m. Vertical turrets are attached to each end of each header, and may be supplied by a two-phase mixture of steam and water. The feeders consist of vertical and horizontal sections of of 50-mm I.D. pipe connected to nozzles distributed along the length of the headers in six groups of feeder banks. Each bank has five feeders — two feeders are attached at 90°, two at 45°, and one downward-oriented, as shown in Figure 8.

The objective of conducting tests in this facility is to provide data that contribute to an improved understanding of two-phase flow phenomena — phase separation in particular — that may occur in CANDU-reactor-type headers under upset or accident conditions.

Figure 9 shows an example of results obtained from this facility [17]. In this experiment, a constant water injection flow of 45 kg/s was mixed with a steam flow and directed into one turret of the inlet header. The steam flow was gradually increased, reducing the level in the header. Figure 9 shows that steam enters Feeder 5 (see void fraction), even though the water level is still above the feeder inlet level (0.50). This phenomenon has been called "steam pull-through."
5.4 INTEGRAL EXPERIMENTS

RD-14M [18] is the fourth generation of integrated loops in a progression from a relatively small-sized to a scaled (vertical) full-elevation simulation of the CANDU system. The facilities were built to improve our understanding of the transient behaviour of a CANDU-reactor-typical heat transport system, and to provide a data base to validate computer codes.

Figure 10 shows a simplified schematic of the RD-14M facility. The facility is a pressurized-water loop, arranged in the basic figure-of-eight geometry of a CANDU pressurized heat transport system, with heated sections representing the reactor core divided into five parallel channels in each pass. The vertical layout of the heated sections, headers, steam generators and pumps provides a full-elevation representation of a typical reactor.

The RD-14M loop simulates the heat generation from the reactor with ten 6-m-long, 1.1-MW horizontal heated sections (HS5 through HS14) connected to end-fitting simulators. The heated sections are divided into two groups of five parallel channels (HS5 through HS9 and HS10 through HS14) representing two passes through a reactor core. Each individual channel contains seven electrically heated fuel-element simulators (FES). The vertical distance between the highest and the lowest heated section in each pass is about 6 m. This corresponds to the full range of elevation differences of fuel channels in a reactor. The remaining three heated sections are centrally located. The heated sections are connected to flow distribution manifolds, or headers (HD5 to HD8), by feeder pipes having a typical reactor geometry. The primary-side pressure in the heated sections and headers is controlled by a pressurizer/surge tank (TK1) using a 100-kW electric heater (HR1). Two high-head centrifugal pumps (P1 and P2) are used to deliver flow rates of the order of 27 L/s. These pumps are capable of simulating, through the use of a ramp generator, the rundown profile of typical reactor pumps following a pump trip caused by a loss of power.

Heat is removed from the primary circuit through two recirculating U-tube-type steam generators or boilers (BO1 and BO2) complete with internal pre-heaters and steam separators. Individual tube diameters and the heat and mass fluxes in the RD-14M boilers maintain an approximate 1:1 scaling with CANDU steam generators, resulting in similar primary- and secondary-side pressure and temperature conditions. Steam generated in the secondary, or shell, side of the boilers is condensed using a cold-water spray in the jet condenser (CD1) and returned as feed water to the boilers. The jet condenser system is used to control the secondary-side pressure at the desired level. The secondary-side pressure can also be reduced linearly using this control system to simulate secondary-side depressurization.

The RD-14M emergency coolant injection system can be operated in several modes representative of the various emergency coolant injection configurations found in CANDU reactors. High-pressure injection can be achieved either from a nitrogen-pressurized tank (TK2) or by using a high-pressure pump (P14). Low-pressure injection from distilled water tanks is obtained using a low-pressure pump (P8). The emergency coolant injection system is connected to all four headers.

Breaks may be simulated using an office plate placed immediately upstream of a fast-acting, 50.8-mm (nominal), remote-control ball valve (MV8) connected to either an inlet or outlet header. Coolant ejected from the loop is transported into the atmosphere through a 508-mm (nominal) blowdown line.

The loop is instrumented extensively with gamma-ray densitometers for fluid density measurements, differential and gauge pressure transducers, thermocouples and resistance temperature detectors. Turbine flowmeters are used to measure single-phase volumetric flow.
rates within the loop and the emergency coolant flow rate from the high-pressure accumulator
emergency coolant injection system. Vortex-shedding flowmeters are used to measure the
flow of emergency coolant injection coolant into the headers from the high-pressure-pumped
emergency coolant injection system.

Experiments performed in our integral facilities include: 1) partial inventory two-phase
thermosiphoning tests, 2) secondary side depressurization tests on a medium scale steam
generator, 3) small- and large-break loss-of-coolant accident simulations, 4) loss-of-flow
simulations, and 5) two-phase flow stability tests.

Figure 11 shows the variation in the maximum FES sheath temperature in RD-14M with the
break size. As shown, a 30-mm break was found to be the "critical" size. This test series is
described in more detail in Reference 18.

6. FUTURE DIRECTIONS

6.1 EXPERIMENTAL PROGRAMS

Future experimental work will largely focus on improving our understanding of the interaction
of multiple parallel heated channels under upset conditions. This is, of course, related to the
blowdown and refill thermalhydraulics of full-size flow headers connecting the feeders.

An increasing emphasis is currently being placed on refining the instrumentation for our test
facilities. An Instrument Development Program has been recently implemented to provide
instrumentation not currently available commercially. For example, conductivity probes are
being developed to accurately measure the level in the RD-14M headers. As well, neutron-
scattering tomography is being evaluated to measure the void and void distribution in the
heated channels of RD-14M and the CWIT facility.

To facilitate easy access to experimental data, a program has been initiated to develop a fully
relational data base of thermalhydraulic data obtained from our experimental programs. Software
is being developed to display the information in a number of formats, including an
"animated" replay of the experiment, to aid the analyst in interpreting the experimental data.

6.2 CODE DEVELOPMENT

Future code development will also focus on accurately predicting the behaviour of the
CANDU header/feeder system under loss-of-coolant accident conditions. A multi-dimensional
representation of the header will be required. At the same time, the computational efficiency
of the codes will have to increase to handle the large number of parallel channels that must
be modelled. Constitutive relations will continue to be refined. An example is the condensa-
tion process when cold water is injected into a steam-filled header.

The codes will be validated using new experimental data as it becomes available.

REFERENCES


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Figure 1: Typical CANDU Major Features
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Inlet Header Water Level Ratio H/D

Void Fraction in the Branch a
Figure 10: Schematic of the RD-14M Test Facility
Figure 11: RD-14M Maximum ES Sheath Temperature for Small, Critical and Large Breaks
Analysis of Selected Two-Phase Flow Phenomena in VVER Reactors with Horizontal Steam Generators

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Abstract

Since 1984 the thermal-hydraulic code ATHLET has been also applied for the analyses of LOCA and transients in VVER plants. The specific design of these plants especially of the steam generator design requires a specific modelling of the phenomena which may occur under LOCA and transient conditions in these plants. Differences in design compared to the design of western reactors have been briefly listed. Specific phenomena occurring under small leak accidents are shortly described. The consideration of the simulation of the boiler-condenser mode illustrates the modelling requirements for a code which may be applied to the prediction of such a thermal-hydraulic behaviour. Facing the lack of experimental data, the reliability of the simulation has been discussed by means of plausibility studies based on the momentum balance for steam and water.

Introduction

The pressurized water reactors of Soviet design differ significantly from those of western design. E.g., these differences concern the course of the main coolant pipes of the primary circuit, the steam generator, the main coolant pumps and the control of main steam relief valves. Among these differences the design of the steam generator with horizontal U-shaped tubes can be considered as the most important difference to western design. On the secondary side of this steam generator a natural steam separation has been utilized.

Due to these differences specific phenomena occur under LOCA conditions. Therefore, the description of the thermal hydraulic behaviour with a code requires an adequate modelling of the specific phenomena involved.
Since 1984 the ATHLET code has been also applied for the analyses of LOCA and transients of VVER plants. The current version of the code ATHLET is applicable for the blowdown phase of large break LOCA, for the whole spectrum of intermediate and small break LOCA including steam generator (SG) tube rupture, and transients.

The current state of ATHLET simulation for steam generator behaviour under boiler-condenser-mode will be pointed out. The results of the ATHLET analysis will be discussed considering the momentum balances for steam and water.

**Differences in Design**

The VVER reactors differ significantly in design compared to reactors of western design. The following list provides a brief comparison between VVER-440, VVER-1000, and the KWU-reactor of type Convoi-1300.

<table>
<thead>
<tr>
<th>Item</th>
<th>Dimension VVER-440/230</th>
<th>VVER-1000/320</th>
<th>Convoi-1300</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal Power</td>
<td>MW</td>
<td>1375</td>
<td>3000</td>
</tr>
<tr>
<td>Power Density in Core</td>
<td>kW/l</td>
<td>84</td>
<td>111</td>
</tr>
<tr>
<td>Net Electrical Output</td>
<td>MW</td>
<td>440</td>
<td>950</td>
</tr>
<tr>
<td>Operating Pressure on Primary Side</td>
<td>MPa</td>
<td>12.5</td>
<td>15.7</td>
</tr>
<tr>
<td>Operating Pressure on Secondary Side</td>
<td>MPa</td>
<td>4.7</td>
<td>6.3</td>
</tr>
<tr>
<td>Number of Loops</td>
<td></td>
<td>6</td>
<td>4</td>
</tr>
<tr>
<td>Turbines per Reactor</td>
<td></td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td>Power Related Water Volume on Primary Side</td>
<td>$m^3/GW_{eq}$</td>
<td>173</td>
<td>108</td>
</tr>
<tr>
<td>Power Related Water Mass on Secondary Side at Full-Power</td>
<td>$Mg/GW_{eq}$</td>
<td>144</td>
<td>53</td>
</tr>
<tr>
<td>Steam Generator (U-Tube)</td>
<td>horizontal</td>
<td>horizontal</td>
<td>vertical</td>
</tr>
<tr>
<td>Steam Generator Tube-Diameter</td>
<td>mm</td>
<td>$16 \times 1.4$</td>
<td>$16 \times 1.5$</td>
</tr>
<tr>
<td>ECCS Injection Location</td>
<td>Cold Leg</td>
<td>Upper Plenum</td>
<td>Hot Leg</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Downcomer</td>
<td></td>
</tr>
<tr>
<td>Reactor Pressure Vessel $H_{stat}/D_{stat}$</td>
<td></td>
<td>3.62</td>
<td>2.84</td>
</tr>
<tr>
<td>Type of Fuel Assemblies</td>
<td>hexagonal (canned)</td>
<td>hexagonal (not canned)</td>
<td>squared (not canned)</td>
</tr>
</tbody>
</table>

Besides these differences the course of elevation of the hot leg of the VVER-440 near to the steam generator has been lowered relative to the hot leg outlet elevation at the pressure vessel. This geometrical detail may lead under LOCA condition to the phenomenon "hot leg sealing" (see fig.1). Contrary to VVER-440 the VVER-1000 and Convoi-1300 do not contain such lowered main coolant pipes on hot leg side.
The steam generator of the VVER-reactors is equipped with horizontal U-tubes. These U-shaped tubes are mounted between vertical inlet and outlet collectors. The hot and cold collectors are positioned in a short distance relative to the horizontal extensions of the U-tubes. A nonuniform heat transfer inducing a nonuniform massflow distribution on secondary side results from that.

**Specific Phenomena**

The differences in design cause under small leak LOCA conditions specific phenomena. Due to the continuous loss of primary inventory the single phase natural circulation will be replaced by the two-phase natural circulation and finally turns to the boiler-condenser mode. Reflux-condenser mode cannot be expected because the lowered course of the elevation of the hot leg of the VVER-440 prevents the backflow of condensate from hot leg to the pressure vessel. The condensate may accumulate within the hot leg and probably seals the hot leg. The heat transfer to secondary side may cease and primary pressure increase. Such phenomenon has been observed in the Hungarian small-scale integral test facility PMK-NVH /BAN-89/.

The heat transfer from primary to secondary side takes place in horizontal U-tubes. The primary coolant enters from hot leg side into the vertical hot collector, distributes over the horizontal U-tubes, reaches the vertical cold collector and leaves the steam generator downward flowing into cold leg. In principle, the U-tubes are very slightly inclined to ensure draining of the tubes for eventual repair works during refuelling. However, it cannot be positively excluded that there could also be an undesired deviation of the U-tube from this orientation. In case of a downward deviation, accumulation of condensate and finally plugging may occur.

The secondary side of the horizontal steam generator is characterized by the natural steam separation. The vertical flow on the SG secondary side crosses the horizontal U-tubes. The heat transfer under normal operational conditions concentrates on a region of the U-tubes which is placed around the hot collector. The mixture level on secondary side reaches its maximum there. As a consequence of the heat transfer distribution rotational flow superposes the vertical upflow and enhances the heat transfer efficiency of the steam generator. Therefore, the effectiveness of the steam generator depends on the power load.

Under a postulated small leak LOCA the depressurization of the primary loop leads to an accumulation of steam in the vertical hot collector first. The development of a mixture level within the collector uncovers the upper part of the horizontal U-tube cluster entrance. A formation of a mixture level on the cold collector may result. Due to this mixture level formation the steam generator reduces in its effective heat transfer area.

Under LOCA conditions the mixture level height on secondary side may fall below the upper bound of the U-tube cluster. The upper part of the U-tube cluster ceases to transfer heat from primary to secondary side. The condensation of steam in the upper part of U-tube cluster ends and steam may accumulate. The heat transfer area of the steam generator reduces. Therefore, it can be expected that the secondary side mixture level controls the position of the mixture level in collectors on primary side.

These list of phenomena mentioned above is not complete but illustrates that LOCA thermal-hydraulics require specific modelling in a code to describe properly the LOCA processes.

**International Efforts on Horizontal Steam Generator Modelling**

An International Seminar was organised in March 1991 in Lappeenranta, Finland, for experts in thermal hydraulic modelling of horizontal steam generators /LAP-91/. The
objectives of the seminar were to collect existing information and experience from different countries in the field of modelling and experimental aspects of the horizontal steam generators in VVER-reactors and to discuss, what kind of experimental data is still required for ensuring verification of the applied computer codes.

As a practical outcome of the seminar, it was agreed that a common calculational exercise should be organized. For this purpose, the designer and manufacturer of the VVER steam generators, OKB Gidropress, volunteered to provide initial and boundary conditions of measured two-phase circulations in the secondary side of the both VVER-440 and VVER-1000 steam generators. Imatra Voima Oy of Finland engaged to act as a coordinating organisation. The initial data has now been distributed to participating organisations in Russia, Germany, Hungary, Czechoslovakia, France and Finland. The applied computer codes include RELAP5/MOD 2 and 3, ATHLET and CATHARE.

Along the heat transfer tubes there are large variations of flow conditions across the tube bundle in the secondary side. This means that it is not practical to define or measure average secondary side flow conditions along the tube length. Therefore OKB Gidropress propose that a calculation should be performed for a local circulation circuit in a certain cross-section of the tube bundle. For this purpose, Gidropress provided experimental water flow rate and void fraction data from the steam generators under nominal conditions. Apparently, the calculations should be still done for the whole shell side volume, since the conditions of one cross-section cannot be defined separately in distinction from the overall flow and heat transfer conditions on the tube and shell side.

The next seminar has been planned to take place in September 1992 for comparing calculational results.

**ATHLET Modelling of a VVER reactor system**

The ATHLET code simulates the thermal-hydraulics of a reactor system by a network of control volumes and junctions (lumped parameter technique) /STE-89/. This simulation technique is supported by basic models like heat conductor and heat transfer models or mixture level model. The technique ensures a high flexibility in the code application.

The technique provides a one-dimensional representation of a reactor primary system. A limited multi-dimensional capability is achieved by parallel one-dimensional lumped parameter control volumes with cross connections between corresponding control volumes.

Recently the plant component and control models have become available in the ATHLET code for VVER-reactors. Among others these models provide simulations of:

- Main steam valves
- Safety relief valves
- Reactor power control
- Part load
- Reactor scram
- Turbine under normal condition
- Turbine trip
- Pressurizer with spray valve
- Pump coast down
The verification of these models are based on ATHLET calculations of plant commissioning tests for the Greifswald Unit 1.

The VVER specific LOCA phenomena require a specific modelling or optimization of code models. Several phenomena have been separately analyzed. E.g., the phenomenological analysis of the boiler-condenser-mode with special attention to the steam generator behaviour has been chosen in order to illustrate the modelling efforts necessary for the VVER-reactor representation.

However, this optimization is lacking VVER-specific experimental investigations in a reactor typical scale qualifying the representation of the VVER reactor system. Therefore, the modelling and optimization process is still considered to be in progress.

**Phenomenological Analysis of the Boiler-Condenser-Mode with ATHLET**

Under a postulated small leak LOCA including station blackout the depressurization of primary loop leads to the sequence of heat removal mode starting with single phase natural circulation, followed by two-phase natural circulation and finally due to continuing loss of coolant inventory the boiler-condenser mode establishes.

Due to the station blackout the main steam valves are closed and secondary feed water is not available. The steam generator relief valves of the VVER-440 open at about 5 MPa and provide the necessary heat release from steam generators.

In the boiler-condenser-mode on primary side the steam flows from the pressure vessel through the hot leg toward the steam generator. The steam condenses within the horizontal steam generator U-tubes and flows as condensate either to the hot collector or to the cold collector depending on the pressure distribution within these U-tubes.

Thus, the boiler-condenser-mode comprises the following thermal-hydraulic processes:

- stratified flow within U-tube clusters
- film flow within the outlet collector of steam generator
- formation of a sharp mixture level on secondary side
- heat transfer from primary side to secondary side
- condensation process on primary side
- evaporation process on secondary side

One loop including hot leg, hot collector, steam generator U-tubes, cold collector and cold leg has been modelled with the ATHLET code in order to phenomenologically analyze these thermal-hydraulic processes (see figure 2: Nodalization Scheme). For that analysis the U-tube cluster of the steam generator has been split into three sub-clusters of equal size but at different elevations. Top height, medium height and bottom height are distinguished. Each subcluster of U-tubes is modelled with 5 control volumes. Therefore, 1845 U-tubes are considered in each subcluster. The 3 x 5 control volumes are modelled with identical flow area. In the real steam generator the U-tubes are not uniformly distributed over the vertical height. The inlet and outlet conditions of each U-tube subcluster is provided by control volumes of equal size representing the corresponding volumes of the cold collector and hot collector.

This modelling of the steam generator intentionally avoids asymmetries in the simulation of the mass flow distribution among the subclusters, and the calculated results
do not reflect a real steam generator behaviour, but it allows to isolate the mechanisms which provoke an asymmetrical flow distribution over the U-tube cluster.

A decay heat of about 25MW corresponding to 15kg/s steam production in the core region at 5.0MPa and distributed over six loops has been assumed in the ATHLET calculations. That is, the considered steam generator receives 2.5kg/s steam in the hot collector. Depending on the pressure difference between primary and secondary side imposed on both sides by pressure boundaries, the steam partly or totally condenses within the U-tubes.

The ATHLET calculation using this symmetrical modelling reveals a condensate flow within the subcluster at lowest position toward the hot collector. The figure 3 illustrates the steady state situation with backward flow in the lower U-tube cluster. The upper U-tube clusters (top height and medium height) show steady state forward flow.

According to the ATHLET calculations this flow depends on the condensation rate produced within the steam generator. Figure 4 "Percentage of Condensate Flow to Hot Leg versus Percentage of Condensed Steam Flow" summarizes the ATHLET calculations with the presented steam generator model. If the total condensation rate exceeds a value of about 25% backward flow may be initiated. The ATHLET calculations suggest a maximum of 32% condensate flow from horizontal U-tube to the hot leg.

**Discussion of Calculated Results**

The backward flow of condensate occurs because the pressure distribution in the cold collector caused by a downward film flow provokes a slight pressure increase at the U-tube outlet relative to the U-tube inlet at the lower U-tube position. The reason for this is that the condensate film in the cold collector is not freely falling into the cold leg. Wall friction and interfacial friction limit the velocity of the falling water film. According to the drift-flux model for vertical pipes the downward flow of water reaches a value of about 1m/s only while the steam flow merely stagnates.

Adequate steam generator experiments to verify the code predictions for the flow and pressure distributions are not available at present. Thus, the reliability of the calculated results has to be seen in conjunction with considerations on momentum balances in the cold collector.

**Momentum Balance for Steam and Water Film**

The consideration of the momentum balance for both the water film and steam phase provides a better understanding of the mechanisms which provoke the backward flow of condensate in the lower U-tubes. Considering a steady state downward flow of the water film in the cold collector the momentum balance for that film is:

\[ \tau_w P_w dz + \tau_f P_f dz - g \rho_w a_w A dz + dp a_w A = 0 \]  \hspace{1cm} (1)

The sign convention shown in figure 5 has been used for equation (1). If the forces are related to the gravitational force of film \( g \rho_w a_w A dz = G_w \) the equation (1) can be expressed as:

\[ x_1 + x_2 + x_3 = 1 \]  \hspace{1cm} (2)

The ratios \( x \) are defined as:

\[ \tau_w P_w dz = x_1 G_w \]
\[ \tau_f P_f dz = x_2 G_f \]
\[ dp a_w A = x_3 G_w \]
\[ g \rho_w a_w A dz = G_w \]

Neglecting the wall friction force of the steam the momentum balance of the steam phase using the sign convention of figure 5 gives:
\[- \tau_i P_i \, dz - g \, \rho_v \, a_v \, A \, dz + dp \, a_v \, A = 0 \quad (3)\]

Relating the forces of equation (3) to the gravitational of the water film \( G_L \) one gets:

\[- x_L \, \frac{a_v \, \rho_v}{a_L \, \rho_L} + x_v \, \frac{a_v}{a_L} = 0 \quad (4)\]

Equation (4) makes use of the relations:

\[ \tau_i \, P_i \, dz = x_L \, G_L \]
\[ g \, \rho_v \, a_v \, A \, dz = G_L \, \frac{a_v \, \rho_v}{a_L \, \rho_L} \]
\[ dp \, a_v \, A = x_v \, G_L \, \frac{a_v}{a_L} . \]

The equations (2) and (4) provide an equation system with 3 unknowns which can be solved if one further relation between the forces is known. Defining the relationship between the wall shear force \( \tau_w \, P_w \, dz \) and the interfacial shear force \( \tau_i \, P_i \, dz \) to be the ratio \( a \):

\[ a = \frac{\tau_i \, P_i \, dz}{\tau_w \, P_w \, dz} \quad (5)\]

one gets an expression for the pressure gradient depending on this ratio \( a \) as:

\[ \frac{dp}{dz} = g \, \rho_v \left( \frac{a \, \rho_v + (1 + a) \, a_v \, \rho_v}{(1 + a) \, a_v + a \, \rho_v} \right) \quad (6)\]

If the interfacial shear force becomes zero \( (a = 0) \) the pressure gradient matches the gravitational force of vapour:

\[ \frac{dp}{dz} = g \, \rho_v \]

If the ratio is equal to one \( (a = 1) \), that is, both frictional forces are equal, for void fractions near to one equation (6) approximately gives:

\[ \frac{dp}{dz} = g \, \frac{\rho_m \, \rho_v}{2} \]

with

\[ \rho_m = a_v \, \rho_v + a_L \, \rho_L \]

Depending on the void fraction \( a_v \) the pressure gradient significantly exceeds the pressure gradient for stagnant steam \( g \, \rho_v \).

According to this simple analysis the downward flowing water film produces due to the non-zero interfacial shear force a pressure increase which exceeds the hydrostatic pressure of steam. Concerning the situation in the cold collector of steam generator this increase of pressure at lower positions of the collector forces the steam to flow from the cold collector into the U-tubes.

In order to quantify the ratio \( a \) the analysis of Wallis for annular flow /WAL-69/ basing on the correlation scheme given by Martinelli leads to:

\[ F_w = \frac{\tau_w \, P_w}{A} = \phi_v^2 \left[ \frac{dp}{dz} \right] \]
\[ F_i = \frac{\tau_i \, P_i}{A} = \phi_v^2 \left[ \frac{dp}{dz} \right] \quad (7)\]

According to the definition of the Martinelli parameter \( X_a \) the ratio of forces \( a \) follows to:
\[ x^2_u = \frac{\phi^2_v}{\phi^2_l} \left[ \frac{dp}{dz} \right]_l \]

\[ \frac{\tau_p}{\tau_w} \frac{dz}{d} = a_v \]

In case of high void fractions \( a_v \) near to 1 the ratio \( a \) approaches 1.

An alternative approach for the estimation of the ratio of frictional forces at the water film \( a \) is to evaluate both the tension at the wall \( \tau_w \) and the interfacial tension \( \tau \) by:

\[ \tau_w = f_w \frac{\rho_L V_L^2}{2} \]  \hspace{1cm} (8)

and

\[ \tau = f \frac{\rho_v (V_V - V_L)^2}{2} \]  \hspace{1cm} (9)

The Fanning friction factor for the wall shear can be determined by Colebrook correlation mentioned in TRU-69/ depending on the pipe roughness (roughness of steel surface is assumed with 0.1mm and 0.01mm) and hydraulic diameter. The hydraulic diameter for the water film \( D_w \) to be used in the Reynolds number is given by

\[ D_w = D a_v \]

Wallis /WAL-69/ suggests to determine the Fanning friction factor for the steam/water interface in dependence of the water fraction \( a_v \) according to:

\[ f_w = 0.005 \left[ 1 + 75a_v \right] \]  \hspace{1cm} (10)

This interfacial Fanning friction factor of (10) significantly differs from those of Bharathan. Bharathan /BHA-78/ observed a dependence of this factor on both film thickness and hydraulic diameter.

\[ f = f(\delta, D) \]  \hspace{1cm} (11)

For the pressure of 5MPa, collector diameter of 0.8m, condensate mass flow rate of 2.5kg/s and stagnant steam flow, the ratio of frictional forces at the water film \( a \) has been plotted in figure 6 versus the film thickness.

The results show that the effect of roughness of steel on this ratio can be neglected, but the effect of the interfacial Fanning friction factor \( f \) on this ratio is essential. Using Wallis' correlation (10) the ratio of forces is about 0.03 whereas the more sophisticated correlation of Bharathan provides values of about 2.0, which are close to the result of equation (7). Therefore, the determination of pressure distribution in a steam generator collector still requires an experimental investigation in a geometric scale close to 1:1 in order to reduce the uncertainties involved.

**Conclusion**

The VVER reactors differ in design compared to reactors of western design. The VVER design, especially the design of the horizontal steam generator, provokes under LOCA conditions specific phenomena which require a specific modelling in a thermal-hydraulic code. Because experimental data in a realistic scale simulating e.g. the steam generator behaviour are not available the verification of code models for the application to VVER reactors has to be reduced on phenomenological studies.

As an example of such phenomenological analyses, the study of the flow phenomenon under boiler-condenser-mode has been performed. This study reveals a condensate flow phenomenon in steam generator U-tubes depending on the flow forces
in the cold collector. According to an ATHLET code prediction assuming a ratio of frictional forces of approximately one, about 1/3 of the total condensate of the steam generator will flow towards the hot collector. Therefore, the possibility of sealing the VVER hot leg due to this condensate flow has to be taken into consideration in an analysis of the boiler-condenser-mode as long as no further experimental investigations on the flow forces are available.

**Nomenclature**

- **A** $m^2$: Cross-sectional area
- **a**: Ratio of frictional forces
- **D** $m$: Diameter of pipe
- **D_l** $m$: Hydraulic diameter of water film
- **D_v** $m$: Hydraulic diameter of steam core in pipe
- $\frac{dp}{dz}$ $N/m^2$: Pressure gradient
- $\left[ \frac{dp}{dz} \right]_l$ $N/m^2$: Pressure gradient, liquid only flowing
- $\left[ \frac{dp}{dz} \right]_v$ $N/m^2$: Pressure gradient, steam only flowing
- $\Delta p^*$: Dimensionsless pressure gradient
- $dz$ $m$: Differential Length
- $F_w$ $N/m^3$: Wall frictional force per fluid volume
- $F_i$ $N/m^3$: Interfacial force per fluid volume
- $G_t$ $N$: Gravitational force of film
- $g$ $m/s^2$: Acceleration due to gravity
- $P_t$ $m$: Perimeter at film/steam interface
- $P_w$ $m$: Perimeter at film/wall interface
- $x$: Multiple of the gravitational force $F_i$
- $X_0$: Martinelli parameter
- $a_t$: Water volume fraction
- $a_v$: Void fraction
- $\phi^*_l$: Martinelli parameter for water
- $\phi^*_v$: Martinelli parameter for steam
- $\rho_l$ $kg/m^3$: Density of water
- $\rho_v$ $kg/m^3$: Density of steam
- $\tau$: $N/m^2$: Interfacial tension
\[ \tau_w = \frac{N}{m^2} \]

Tension at wall

References


/LAP-91/ Proceedings of International Seminar of Horizontal Steam Generator Modelling, Research Papers 18, Volumes 1 and 2, Lappeenranta University of Technology, March 11-13, 1991, Lappeenranta, Finland


Fig. 2: Nodalization Scheme for the Horizontal Steam Generator
WWER-440 Steam Generator under LOCA Conditions
Primary Side Pressure = 5.15 MPa
Secondary Side Pressure = 5.00 MPa

Fig. 3: Steam and Water Mass Flow Rate Distribution over U-Tubes
Steam Generator under Boiler–Condensor–Mode

Fig. 4: Percentage of Condensate Flow to Hot Leg versus Percentage of Condensed Steam Flow
Fig. 5, a: Momentum Balance of Water Film

Fig. 5, b: Momentum Balance of Steam
Fig. 6: Ratio of Frictional Forces Acting on Water Film

\( D = 0.8 \text{ m}; P = 5.15 \text{ MPa}; G_\text{L} = 2.5 \text{ kg/s}; G_\text{V} = 0 \)
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