NUCLEAR ENERGY AGENCY
COMMITTEE ON THE SAFETY OF NUCLEAR INSTALLATIONS

IN-VESSEL CORE DEBRIS
RETENTION AND COOLABILITY

Workshop Proceedings

3-6 March 1998
Garching near Munich, Germany
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NUCLEAR ENERGY AGENCY

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This is achieved by:

− encouraging harmonization of national regulatory policies and practices, with particular reference to the safety of nuclear installations, protection of man against ionising radiation and preservation of the environment, radioactive waste management, and nuclear third party liability and insurance;
− assessing the contribution of nuclear power to the overall energy supply by keeping under review the technical and economic aspects of nuclear power growth and forecasting demand and supply for the different phases of the nuclear fuel cycle;
− developing exchanges of scientific and technical information particularly through participation in common services;
− setting up international research and development programmes and joint undertakings.

In these and related tasks, the NEA works in close collaboration with the International Atomic Energy Agency in Vienna, with which it has concluded a Co-operation Agreement, as well as with other international organisations in the nuclear field.

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COMMITTEE ON THE SAFETY OF NUCLEAR INSTALLATIONS

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CSNI constitutes a forum for the exchange of technical information and for collaboration between organisations which can contribute, from their respective backgrounds in research, development, engineering or regulation, to these activities and to the definition of its programme of work. It also reviews the state of knowledge on selected topics of nuclear safety technology and safety assessment, including operating experience. It initiates and conducts programmes identified by these reviews and assessments in order to overcome discrepancies, develop improvements and reach international consensus in different projects and International Standard Problems, and assists in the feedback of the results to participating organisations. Full use is also made of traditional methods of co-operation, such as information exchanges, establishment of working groups and organisation of conferences and specialist meetings.

The greater part of CSNI’s current programme of work is concerned with safety technology of water reactors. The principal areas covered are operating experience and the human factor, reactor coolant system behaviour, various aspects of reactor component integrity, the phenomenology of radioactive releases in reactor accidents and their confinement, containment performance, risk assessment and severe accidents. 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 nuclear power plant incidents.

In implementing its programme, CSNI establishes co-operative mechanisms with NEA’s Committee on Nuclear Regulatory Activities (CNRA), responsible for the activities of the Agency concerning the regulation, licensing and inspection of nuclear installations with regard to safety. It also co-operates with NEA’s Committee on Radiation Protection and Public Health and NEA’s Radioactive Waste Management Committee on matters of common interest.
CSNI-WS on In-Vessel Core Debris Retention and Coolability

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B SUMMARY AND CONCLUSIONS

1. Introduction

1.1 Sponsorship

The CSNI Workshop on In-Vessel Core Debris Retention and Coolability, held on 3rd-5th March 1998 in Garching near Munich, Germany, was sponsored by the Committee on the safety of Nuclear Installations (CSNI) of the OECD Nuclear Energy Agency (NEA). It was organised in collaboration with Gesellschaft fuer Anlagen- und Reaktorsicherheit (GRS) and the Technical University of Munich, Institute A for Thermodynamics.

1.2 Background and Objectives

In the spring of 1994 an OECD Workshop on Large Pool Heat transfer was held in Grenoble. The scope of this workshop was the investigation of (1) molten pool heat transfer, (2) heat transfer to the surrounding water, and (3) the feasibility of in-vessel core debris cooling through external cooling of the vessel. Since this time, experimental test series have been completed (e.g., COPO, ULPU, CORVIS) and new experimental programs (e.g., BALI, SONATA, RASPLAV, debris and gap heat transfer) have been established to consolidate and expand the data base for further model development and to improve the understanding of in-vessel debris retention and coolability in a nuclear power plant.

Discussions within the CSNI’s PWG-2 and the Task Group on Degraded Core Cooling (TG-DCC) have led to the conclusion that the time was ripe for organizing a new international Workshop with the objectives

• to review the results of experimental research that has been conducted in this area
• to exchange information on the results of member countries experiments and model development on in-vessel core debris retention and coolability.
• to discuss areas where additional experimental research is needed in order to provide an adequate data base for analytical model development for core debris retention and coolability.

The scope of this workshop was limited to the phenomena connected to in-vessel core debris retention and coolability and did not include steam explosion and fission product issues.

The workshop was structured into the following sessions

1. Key note papers
2. Experiments and model development
   2.1 Debris bed heat transfer
   2.2 Corium properties, molten pool convection and crust formation
   2.3 Gap formation and gap cooling
   2.4 Creep behaviour of reactor pressure vessel lower head
2.5 Ex-vessel boiling and critical heat flux phenomena
3. Scaling to reactor severe accident conditions and reactor applications

The program committee, nominated by the TG-DCC and approved by PWG2, was:

Dr. Ali-Reza Behbahani, USNRC
Mr. Andrzej Drozd, NEA (secretary)
Dr. Sang-Baek Kim, KAERI, Korea
Dr. Jean-Claude Micaelli, IPSN, France
Dr. Timo Okkonen, ABB, Sweden
Dr. Jun Sugimoto, JAERI, Japan
Dr. Klaus Trambauer, GRS, Germany (chairman)
Dr. Harri Tuomisto, IVO, Finland

The meeting was attended by 91 participants from 15 OECD countries and one non-OECD country: Germany (28), France (22), Finland (7), USA (6), Italy and Sweden (5 each), Russia (4), Japan (3), Belgium, Czech Republic and Hungary (2 each), Austria, Korea, Netherlands, Switzerland and UK (1 each).
2. General remarks and Conclusions

2.1 General Conclusions

Compared to the previous workshop held in Grenoble in 1994, large progress has been made in the understanding and the modelling of several phenomena involved in the domain of interest. They concern:

- the corium properties
- the molten pool convection
- the gap formation and cooling
- the creep behaviour of the lower head
- the ex-vessel critical heat flux

and they are discussed in the session specific conclusions (chapter 2.2).

Some conclusions have been reached and some outstanding questions have been identified during the general discussion:

- Plant specific studies performed on core melt retention indicate with high confidence level that the in-vessel retention concept is possible for low (~600 MWₑₑ) power reactors.
- Convective corium pool behaviour with all chemical and physical aspects requires still confirmatory research. Such as being carried out in OECD Rasplav project. The pool formation and the initial conditions in the lower plenum are plant and accident sequence dependent and are subject to large uncertainties.
- Since in-vessel coolability cannot be ensured (e.g., for high power plants), it is highly desirable to understand RPV lower head mechanical behaviour (i.e., size, location and time to failure) and to improve the modeling of creep rupture behaviour.
- For severe accident analyses different approaches are useful:
  - Obtain physical understanding by well designed experiments and detailed code calculations
  - Apply integral codes to obtain scenario and system effects such as timing
  - Complement remaining shortcomings by separate engineering calculations when necessary
- One of the most important work is to develop a consistent severe accident management strategy for each individual plant. This ensures the above discussed chemical and physical phenomena do not cause confusion among the reactor safety society.

2.2 Session specific conclusions

2.2.1 Debris bed heat transfer

Progress in understanding and modelling is underway, however no new result impacted the feasibility of the In Vessel Retention concepts.

No priority needs have been identified, nevertheless efforts should be maintained to enrich the knowledge of basic phenomena involved in severe accidents, and increase the capability of numerical tools to predict scenarios.
Compared to the results of the previous workshop, it can be recognised that the scope of the analytical tools has been enlarged to consider the heat transfer to two-phase water flow. The debris formation was not covered in the previous workshop; the FARO facility provided quantitative results, but up to now there are difficulties to transpose them to reactor scale.

2.2.2 Corium properties, molten pool convection and crust formation

Some corium properties have been measured in RASPLAV project as presented in one of key note papers, and physico-chemistry and corium properties have been discussed. Although new findings are being obtained, a wide variety of property measurements and analysis will be needed.

The effect of mushy zone on the heat transfer could be important and should be clarified.

Concerning molten pool natural convection and crust formation, extensive experimental and analytical studies are being conducted. More careful attention should be paid for synthesising the results of several different experiments. Also emphasise should be paid on the physical and chemical mechanism, such as non-eutectic mixtures and phase segregation, as well as for the model development and code assessment. The relevance of enhanced natural convection due to boiling processes with bubble formation has not been verified yet.

The chemo-physical behaviour of the corium melt, the melt segregation and the formation of a metal-rich layer on the top of the ceramic melt pool were discussed already in the previous workshop. The data base for these phenomena has been significantly enlarged, but confirmatory research is still needed, especially for the corium properties and the transposition of simulant test results to the reactor problem.

2.2.3 Gap formation and gap cooling

The formation of gaps with a width in the order of a couple of mm has been confirmed after relocation of corium melt masses up to 200 kg in ALPHA, LAVA, FAI and FARO experiments.

The total power transfer to the fluid in narrow gaps is limited by dry-out rather than critical heat flux.

The measured maximum heat flux (100 to 500 kW/m²) which results in gap dry-out increases with gap width, pressure and sub-cooling. At higher heat flux longer local dry-out periods result in wall temperatures higher than Leidenfrost temperature and spontaneous re-wetting is not possible.

Discrepancies regarding the dependency of maximum heat flux on pressure between different test facilities should be investigated.

Internal gap cooling requires long-term availability of water and stable crust formation. The latter is questionable for larger melt pools and for metal rich-melts. The tendency of better cooling conditions at higher pressure due to gap formation and higher heat flux is contrary to
depressurisation as severe accident measures. Its safety relevance was questioned, nevertheless internal gap cooling seems to be important for specific sequences, as seen in TMI-2.

- In the previous workshop this phenomenon was not covered, except in one presentation (not included in the proceedings) where this was discussed as a possible explanation for the cooling of the hot-spot in the TMI-2 accident, which was found in the TMI-VIP. Since then, extensive progress has been achieved in understanding gap formation and gap cooling behaviours. This confirms that this phenomenon is a possible explanation of cooling of the hot spot of TMI-2, nevertheless the formation and stabilisation conditions are not enough understood for a practical applications to the reactor.

2.2.4 Creep behaviour of reactor pressure vessel lower head

Further knowledge of the RPV lower head material properties are needed for adequate modeling of strain and stress behaviour.

Additional integral RPV lower head structural integrity experiments scaled properly for wall thickness need to be carried out.

Further experimental study on the RPV lower head creep behaviour needs to be performed at lower pressures.

- In the previous workshop there was presented only one structural analysis of the failure of RPV wall due to thermal and mechanical loads. Now the experimental data base is considerably enlarged with physically based thermal boundary conditions.

2.2.5 Ex-vessel boiling and critical heat flux phenomena

The experiments confirmed high heat transfer coefficients for ex-vessel boiling.

The plant specific design of RPV, cavity and insulation structure have important impact to the maximum heat transfer to the external coolant. The gravity driven flow is sensitive to steam venting.

Feasibility studies of ex-vessel cooling must consider the effect of reduced heat transfer due to degradation of insulation, unavailability of flow paths, etc.

- In the previous workshop this phenomenon was covered by one session, presenting data from two experimental facilities and planned investigations. The data base is now considerably enlarged and the cooling process is quite well understood.

2.2.6 Scaling to reactor severe accident conditions and reactor applications

The results and the extensive expert review process of the AP-600 in-vessel retention study have been presented and discussed.
The reasonability of applying integrated severe accident codes for physical studies have been demonstrated. It was recognised that integrated codes are sometimes applied outside their original scope and that the practical assessments and decisions concerning severe accident management require sound physical understanding of the phenomena that can be obtained through separate effect experiments and analytical work.

- Compared to the previous workshop the analytical tools have been improved. The employed models have still some uncertainties e.g. with respect to accident scenario dependency. These uncertainties can be evaluated by varying sensitive parameters.

### 2.3 Questions and Answers

According to the announcement of the workshop the following questions were discussed:

- **Are there accident management measures which significantly delay RPV failure due to the thermal and mechanical loads of the core debris?**
  
  *Answer:*
  
  There is no change in the strategy to bring in water into the primary system as soon as it is feasible.

- **What can we do for existing LWRs to retain core debris in vessel?**
  
  *Answer:*
  
  The answer is very plant specific. In-vessel melt retention depends on power level, geometrical features and the availability of water. If the cavity is to be flooded, one should show the sufficiency of cooling for all pertinent scenarios or, when this fails, study carefully the consequences in case of vessel failure.

- **How to retain the core debris in the vessel of future reactor designs without external flooding?**
  
  *Answer:*
  
  The only way is to get sufficiently early long-term availability of water, and to bring it in the vessel in order to prevent core debris relocation into the lower plenum. For sequences significantly delayed with water availability, internal core catchers have been proposed, but up to now they do not guarantee efficient corium cooling.

- **What is the upper limit of thermal power which guarantees the core debris retention in the RPV?**
  
  *Answer:*
  
  In general terms the thermal power depends on nominal power, accident sequence, size of vessel, corium composition, ex-vessel conditions and the necessary failure safety margin.

- **What are the additional needs for experiments, model development, and code development?**
  
  *Answer:*
  
  Besides the needs formulated in the general discussion above, research on national level is determined by specific requirements which consider verification or confirmation of physical and chemical models, the validation of computer codes as well as the maintenance of knowledge and the qualification of experienced staff in nuclear reactor safety.
3. Summary of presented papers and session specific conclusions

3.1 Key note papers

Prof. Unger gave a presentation on various aspects of the use of nuclear power and the use of energy world-wide. After that he focused on key phenomena of late-phase melt progression and their relationship to severe accident management and code modelling. Also the key issues of early-phase core degradation were covered. From the accident management point of view, the main questions are the availability and efficiency of water injection into the vessel before the potential for a vessel failure, as well as the feasibility of external vessel cooling to prevent a vessel failure even with a complete core meltdown. There are uncertainties associated with some of the key phenomena, and their modelling in codes is somewhat different. The implementation of severe accident management requires plant-specific studies and clear operator instructions.

Prof. Theofanous gave a presentation on the in-vessel melt retention as a severe accident management strategy, with a particular focus on external vessel cooling. Design-specific assessments, including detailed phenomenological studies, have been performed for the Loviisa plant and the AP-600 design. The assessments have been subjected to extensive expert reviews and also regulatory reviews. He made the conclusion that the above assessments are well based on both fundamental and practical considerations, and that the strategy is proven for the above low power reactor designs. Closer examinations is necessary for large-power reactors as well as the potential applications for ex-vessel (core catcher) situations. The assessments need to be systematic and transparent, and they need to cover both thermal aspects and potential steam explosion loadings against the vessel lower head. Thermal studies are getting to become fairly mature, while the steam explosion analyses require (and have already stimulated) special modelling advancement. Real-material tests, such as those of the OECD Rasplav project, are important in the sense that they provide generically applicable information about the core melt behaviour and properties.

Dr. Seiler gave a presentation on the GAREC research programme that is being carried out to understand the key processes that would affect the in-vessel melt retention, in particular for large-power reactor designs. The presentation covered the scenarios and the phenomena that are involved during the time frame from core meltdown to melt attack against the vessel. Several questions of importance have been studied and their influence can now be estimated, mostly in a way that favours in-vessel melt retention. Some special issues such as the barium release from the melt pool, as well as their potential influence on decay power generation and thermal margins, need to be further investigated. In general, the margins for vessel failure are lower in a large-power reactor and therefore further research is necessary. The main open questions are the focusing effect (metallic layer heat flux) and the fuel-coolant interactions (in case of late water injection), as well as some reactor-specific questions about the core melt relocation process and the subsequent melt attacks and the initial debris configuration in the vessel lower head.
Dr. Asmolov gave a presentation on the progress and the latest findings of the OECD Rasplav project. A large number of experiments have been performed, including both simulant and real-material tests. Data has been obtained for the properties of various core melt compositions and the behaviour of prototypic core melt in a relatively large-scale melting facility. The latest finding is the stratification or segregation behaviour that has been observed for a mixture of $\text{UO}_2$, $\text{ZrO}_2$, and Zr. Phase 2 of the OECD Rasplav project is underway.
3.2 Experiments and model development

3.2.1 Debris bed heat transfer

The understanding of phenomena involved in debris bed is important since debris beds constitute the most probable initial condition for corium molten pool formation in the lower head of a RPV. The main questions addressed are the debris bed formation, its coolability (remelting, steam production, drying of lower head), its transition to a molten pool (time and temperature), and the molten pool configuration (stratification).

Five papers were presented in this session, two experimental programmes, two modelling programmes and one uncertainty study:

Magallon made a presentation of FARO results. The formation and the cooling of corium debris beds resulting from corium melt jet quenching tests have been investigated. The influence of several parameters has been analysed (such as pressure, water height, corium mass). The interpretation and transposition to the reactor remain to be done.

Horner presented a more analytical programme dealing with thermal hydraulics and heat transfers in a internally heated debris bed with the evidence of multi-dimensional effects.

Buck presented the WABE-2D and MESOCO-2D codes to be coupled with ATHLET-CD. They treat the two phase flow in the debris bed, and the melting and relocation process within the debris bed respectively.

Fichot presented ICARE2 code and more precisely the melting and relocation module, a reactor application has been discussed.

Schaaf presented an uncertainty and sensitivity analysis made with AIDA code. He demonstrated the interest of such a probabilistic approach for the identification of sensitive processes to be modelled, regarding the lower head thermal ablation.

During the concluding discussion of the session, the following main points were raised:

- Progress in understanding and modelling is underway, however no new result impacted the feasibility of the In Vessel Retention concepts yet.
- No priority needs have been identified, nevertheless efforts should be maintained to enrich the knowledge of basic phenomena involved in severe accidents, and increase the capability of numerical tools to predict scenarios.

3.2.2 Corium properties, molten pool convection and crust formation

The objective of this session was to review the corium properties, and to understand the molten pool natural convection and crust formation behaviours in the RPV lower head.

Eight papers were presented in this session, one for corium properties, two experimental and the remaining five analytical works.
The paper presented by Froment and Gueneau deals with physics - chemistry and corium properties that may have consequences on In-Vessel retention capabilities. The separation into two layers (as observed in Rasplav) may be either due to a miscibility gap, or due to some other mechanism (density separation). If the metallic part relocates in the upper part of molten pool, this is expected to decrease the focusing effect, if not more than 50 % zirconium has been oxidised. Significant barium release observed in VERCORS experiments may have influence on the reduced residual power in corium pool. Complementary further investigations are found to be necessary.

Existence of a mushy zone was discussed by Seiler. Using a theoretical formulation with assumptions, a mushy zone cannot exist in thermal-hydraulic steady state condition, as a conclusion. The validity of the assumptions was discussed and further investigation will be continued.

The paper presented by Helle reported results from COPO II-Lo and COPO II-AP experiments with homogeneous pool. The heat transfer coefficients obtained are higher than those in the earlier experiments such as Steinberner and Reineke, COPO I and ACOPO. Several possible reasons for this discrepancy have been investigated.

The paper presented by Bonnet and Spindler deals with BALI experiments for thermal-hydraulic behaviour of molten pool and its numerical simulation with TOLBIAC code. BALI experiments with full scale 2D capabilities showed good agreement with COPO II experiment. For shallow metallic layer, a separate effect experiment showed that a radial temperature gradient in the fluid layer reduces the focusing effect. This reduction is however weak (about 20 % reduction of heat flux on the wall for a 5 cm thick layer).

The paper presented by De Cecco reports TOLBIAC code simulation being pursued for molten salt Rasplav experiments. Further code assessment is ongoing for better agreement between calculations and experimental data.

The paper presented by Dinh reports the first SIMECO experimental results on in-vessel melt pool formation and on heat transfer with and without metallic layer. In a slice-type geometry with a semicircular section and a vertical section, water and eutectic salt as melt simulants were employed. the MVITA code has been applied for pre-test and post-test analysis.

The paper presented by Nourgaliev deals with numerical investigation of turbulent natural convection in an internally-heated melt pool and metallic layer. Turbulent models have been studied and Direct Numerical Simulation method was applied. It was demonstrated that one-point closure turbulence models are unable to describe natural convection flows and heat transfer in unstable / stable-stratified fluid. Future works will focus on the development and validation of Large-Eddy Simulation (LES) approach.

The paper presented by Chudanov reports the current status and validation of CONV2D & 3D code for the analysis of convection / diffusion processes accounting for melting in a wide range of geometric parameters and boundary conditions for laminar, transitional and turbulent regimes. Extensive validation activities such as for Rasplav and ACOPO experiments, have been conducted.
The paper presented by Kondratenko deals with the heat transfer similarity between three-dimensional volume and quasi-two-dimensional analogy employed in simulant experiments in a thin slice geometry. It confirmed the proper design of the employed test facilities.

During the concluding discussion of the session, the following main points were raised:

- Some corium properties have been measured in RASPLAV project as presented in one of key note papers, and physico-chemistry and corium properties have been discussed. Although new findings are being obtained, a wide variety of property measurements and analysis will be needed.

- The effect of mushy zone on the heat transfer could be important and should be clarified.

- Concerning molten pool natural convection and crust formation, extensive experimental and analytical studies are being conducted. More careful attention should be paid for synthesising the results of several different experiments. Also emphasise should be paid on the physical and chemical mechanisms, such as non-eutectic mixtures and phase segregation, as well as for the model development and code assessment. The relevance of enhanced natural convection due to boiling processes with bubble formation has not been verified yet.

3.2.3 Gap formation and gap cooling

The objectives of this session was to investigate the processes of gap formation between core debris and the lower head wall as well as the hydrodynamics of the gap flow and limitations of heat transfer to the fluid.

Six paper were presented in this session, two on gap formation, three on gap thermal hydraulics and one on a planned integral test facility.

The paper presented by Maruyama deals with the fragmentation and quenching of 30 to 50 kg Al₂O₃ in water under 1.3 MPa ambient pressure as well as numerical analysis with CAMP code. The debris behaviour is similar to that observed in FARO test facility with particle and cake formation. There is no sticking of the cake with the steel wall and gaps with 1 to 2 mm width were found everywhere.

The paper from Kang et al was presented by S.B. Kim. It deals with the quenching of 30 to 40 kg Al₂O₃ and iron in water under 1.7 MPa ambient pressure in the LAVA test facility. a gap was formed at the interface between the debris and steel wall. A significantly rapid temperature reduction, probably due to water ingression, occurred only in the test with ceramic melt but not in case of metallic melt.

The paper presented by Sehgal describes the FOREVER test facility which employs a 1/10 linear-scaled carbon steel vessel. It is planned to perform experiments with 20 litres binary oxidic melts to study gap formation due to vessel creep. Gap cooling experiments will then be performed. Scaling considerations have been investigated and pre-test calculations have been performed with MVITA and ANSYS codes, respectively, for thermal loadings and for creep behaviour.
Strizhov presented the paper of Kobzar et al, which deals with a separate effects test for gap cooling. Varied parameters are height and width of gap, ambient pressure, hydrostatic head and one or two sided heating. At higher heat flux periodic oscillations with very low frequency were observed. Two side heated gap locally dries out at 30 % less power than one side heated gaps. Experiments with inclined gaps are planned.

S.B. Kim presented the paper of Jeong et al. It deals with various small scale experiments to visualise the gap flow and to measure the maximum heat flux for gap dry-out (CHFG test series). The gap dried out at relatively low power compared to other experiments. The dependency of dry-out on pressure is relatively weak (Koizumi correlation).

The paper presented by Koehler deals with a large scale experiment with gap cooling in a TMI-2 like configuration. Varied parameter are gap width, pressure, sub-cooling and heat flux (max 550 kW/m$^2$). High heat fluxes are realised with small gap widths. After start of the heating often periodic oscillations with 1/20 to 1/30 Hz are observed. The pressure amplitude corresponds to static head difference between liquid and vapour filled gap.

During the concluding discussion of the session, the following main points were raised:

• The formation of gaps with a width in the order of a couple of mm has been confirmed after relocation of corium melt masses up to 200 kg in ALPHA, LAVA, FAI and FARO experiments.

• The total power transfer to the fluid in narrow gaps is limited by dry-out rather than critical heat flux.

• The measured maximum heat flux (100 to 500 kW/m$^2$) which results in gap dry-out increases with gap width, pressure and sub-cooling. At higher heat flux longer local dry-out periods result in wall temperatures higher than Leidenfrost temperature and spontaneous re-wetting is not possible.

• Discrepancies regarding the dependency of maximum heat flux on pressure between different test facilities should be investigated.

• Internal gap cooling requires long-term availability of water and stable crust formation. The latter is questionable for larger melt pools and for metal-rich melts. The tendency of better cooling conditions at higher pressure due to gap formation and higher heat flux is contrary to depressurisation as severe accident measures. Its safety relevance was questioned, nevertheless internal gap cooling seems to be important for specific sequences, as seen in TMI-2.

3.2.4 Creep behaviour of reactor pressure vessel lower head

The objective of this session was to understand the creep behaviour of the RPV lower head under the combined effect of pressure and temperature. Thus, enhancing our knowledge of the gap formation between the RPV lower head vessel wall and corium crust, and more importantly, if RPV lower head were to fail, what would be the size, location and time to failure.

Five papers were presented in this session, one experimental and the remaining four analytical.
The paper presented by T. Y. Chu discusses experimental investigation of creep behaviour of RPV lower head under the combined effect of pressure and temperature performed at Sandia National Laboratories (SNL) on a 1:5 scaled lower head. Size, location and time to failure depend on the heat flux distribution on the inside of the lower head. It appears that the failure location might be related to slight variations in the wall thickness caused by manufacturing of the vessel.

In the paper presented by Autrusson, two simplified models were proposed to estimate the creep rupture of the RPV lower head for the flooded and dry cavity. Application of these models to SNL’s lower head failure experiments are presently underway.

Strizhov presented the paper by Yamshchikov et. al., dealing with application of finite element code (HEFEST) to SNL’s first two lower head experiments. Result of this analyses was supplemented with the studies of uncertainties of the prediction with LOHEY code.

In the paper presented by Sievers results of a parametric study concerning the influence of the wall thickness on the creep behaviour of spherical shells simulating the RPV lower head were summarised. It shows that creep failure in a scaled thin shell under adequate loading conditions is reached much earlier than in a RPV lower head, because significant temperature gradients are build up in the thick shell.

The paper presented by Bhandari deals with the mechanical behaviour of the RPV low-alloy steel materials in late phase external cooling during a severe accident. The long-term cooling introduces strong stress changes which may result in local damages progression. The consequences of this must be investigated.

During the concluding discussion of the session, the following main points were raised:

- Further knowledge of the RPV lower head material properties are needed for adequate modeling of strain and stress behaviour.
- Additional integral RPV lower head structural integrity experiments scaled properly for wall thickness need to be carried out.
- Further experimental study on the RPV lower head creep behaviour needs to be performed at lower pressures.

3.2.5 Ex-vessel boiling and critical heat flux phenomena

The objectives of this session was to quantify the heat transfer of ex-vessel flooding.

Two experimental papers were presented in this session, one including analytical work.

The paper presented by Cheung deals with 0.3 m diameter ex-vessel boiling experiment. The buoyancy driven co-current two phase flow induced by the boiling process in the annular channel enhanced nucleate boiling heat transfer coefficient along the hemispherical downward surface.
The paper presented by Rouge deals with the analytical full scale forced convection experiment SULTAN with a wide range of parameters. CHF results obtained on SULTAN are consistent with those obtained on ULPU. Based on the measured data a Critical Heat Flux (CHF) correlation were developed, in terms of pressure, mass velocity, steam quality, gap width, and gap inclination. 3-D simulation with the CATHARE code of SULTAN experiment showed the capability of revealing the 3-D effect in the experiment.

During the concluding discussion of the session, the following main points were raised:

- The experiments confirmed high heat transfer coefficients for ex-vessel boiling.
- The plant specific design of RPV, cavity and insulation structure have important impact to the maximum heat transfer to the external coolant. The gravity driven flow is sensitive to steam venting.
- Feasibility studies of ex-vessel cooling must consider the effect of reduced heat transfer due to degradation of insulation, unavailability of flow paths, etc.
3.3 Scaling to reactor severe accident conditions and reactor applications

The session consisted of three papers on in-vessel retention of PWRs by external flooding and of two papers on the BWR lower head penetration failures.

Rempe presented the paper summarising the U.S. NRC sponsored review of Prof. Theofanous’ report on in-vessel retention (IVR) and coolability for AP-600 like design. The impact of different material properties and melt configurations on IVR were included.

C. Cognet reported the application of MAAP4 to study in-vessel retention of a large PWR by external flooding. He concluded that the study was not capable of demonstrating sufficient thermal margins to support the in-vessel retention concept for all severe accident scenarios.

Caroli reported the analyses on thermal hydraulic behaviour of the molten pool and thermal conditions of the vessel wall for three different melt configurations in a PWR vessel lower head with penetrations.

Lindholm reported the Nordic study for a BWR concerning the debris bed formation, the debris bed cooling by reflooding and the likely failure mechanism of the lower head. The work was done by comparing predictions with integral codes MAAP4 and MELCOR, and by applying specific thermal and structural code PASULA for the lower head failure predictions.

Stempniewicz reported two studies aiming at prediction of the BWR lower head penetration failure. The first part dealt with the finite element ANSYS analysis of the CORVIS drain line experiments. The second part studied lower head penetration failures of GKN Dodewaard with MAAP and MELCOR codes.

During the concluding discussion of the session, the following main points were raised:

- The results and the extensive expert review process of the AP-600 in-vessel retention study have been presented and discussed.
- The reasonability of applying integrated severe accident codes for physical studies has been demonstrated. It was recognised that integrated codes are sometimes applied outside their original scope and that the practical assessments and decisions concerning severe accident management require sound physical understanding of the phenomena that can be obtained through separate effect experiments and analytical work.
Key Phenomena of Late Phase Core Melt Progression, Accident Management Strategies and Status Quo of Severe Fuel Damage Codes

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General Remarks
Nuclear power has become an important electrical energy source in many countries since its introduction four decades ago. At present 441 nuclear power plants worldwide are in operation, as indicated in Figure 1 [1, 2], to produce electricity in 32 countries.

![Graph showing the number of nuclear reactors in operation from 1956 to 2015](image)

**Figure 1. Number of Nuclear Reactors Worldwide in Operation [2]**

As shown in Figure 2 these plants cover a total electricity generating capacity of 370 GW\(_{el}\), which represents 10% of the world's overall capacity. The current annual nuclear electricity production is given by 2375 TWh\(_{el}\) (see Figure 3), reflecting 17% of the world's total electricity production. There are 17 countries in which one-quarter or more of the electricity is produced by nuclear power plants, e.g. in Lithuania nearly 85% percent of the country's electrical energy is produced by nuclear power, followed by France (75%), Belgium (55%) and Sweden (47%).

Development of commercial nuclear power utilization started in 1951 when the EBR–I fast breeder research reactor started electricity production on December 19th; three years later in 1954 a main electricity producing reactor station, the 5 MW\(_{el}\) Obninsk light water graphite reactor, was put into operation. Two years later in 1956 the first commercial nuclear power plant at Calder–Hall went critical with an electricity generating capacity of 38 MW\(_{el}\).
This reactor and three identical units at the same location are still in operation, however, equipped with an upgraded reactor each of which has now 50 MW_{el} of electricity generating capacity and nearly 40 years of operation experience.

The following 15 years were characterized by construction and testing of large prototypic nuclear power stations. Different types of reactors were designed, constructed and put into operation to gain experiences and determine in practice their advantages and disadvantages. At that time industry, politicians and the public were convinced that nuclear power represented a worldwide good solution for cheap and reliable human energy supply.
Consequently, nuclear power plants were extensively built up in the 1970’s, based on a worldwide nuclear electricity generating capacity of 30 GW_{el} and 1.5% share of the world’s overall electricity production. Plants with 1 GW_{el} design generating capacity and more were put into operation. Up to the beginning of the 1980’s the average expansion rate of nuclear power capacity and electricity production increased to an annual growth rate of 20%. At that time, 150 new plants of 110 GW_{el} additional generating capacity had been connected to the grid, resulting in a total of 250 reactors with 140 GW_{el} generating capacity.

In the late 1980’s this expansion rate dropped and less new plants were ordered due to lower growth trends in energy demands. However, within this decade most of the nuclear power plants with large capacities of up to 1.5 GW_{el} went into service. Thus, there was a remarkable increase in electricity generating capacity growth from about 140 GW_{el} up to 340 GW_{el}, with the total number of nuclear reactors in operation increasing by 160 plants, resulting from the effects of the two oil price crises in the 1970’s and early 1980’s.

A further remarkable fact was the commercial success of the light water reactor technology which covered more than 90% of the additionally connected electricity generating capacity since the 1970’s. At the beginning of the 1990’s, 35 new nuclear power plants worldwide were put into operation and the world’s nuclear capacity reached a level of 350 GW_{el}. Currently, 40 plants with a generating capacity of more than 32 GW_{el} are under construction.

In order to put events at nuclear installations into a proper perspective and to ease common understanding about the safety significance of such events among the nuclear community, the media, and the public, the International Nuclear Event Scale (INES), as shown in Figure 4, has been designed by an international group of experts convened jointly by the International Atomic Energy Agency (IAEA) and the Nuclear Energy Agency (NEA) of the Organization for Economic Cooperation and Development (OECD).

<table>
<thead>
<tr>
<th>Level</th>
<th>Descriptor</th>
<th>Examples</th>
</tr>
</thead>
<tbody>
<tr>
<td>7</td>
<td>Major Accident</td>
<td>Chernobyl Unit 4 Reactor, USSR, April, 1986</td>
</tr>
<tr>
<td>6</td>
<td>Serious Accident</td>
<td>Windscale Pile (GGR), USSR, 1957</td>
</tr>
<tr>
<td>5</td>
<td>Accident with Off-Site Risk</td>
<td>Windscale Pile (GGR), GB, 1957</td>
</tr>
<tr>
<td>4</td>
<td>Accident without Significant Off-Site Risk</td>
<td>Three Mile Island Unit 2 (PWR), USA, March, 1979</td>
</tr>
<tr>
<td>3</td>
<td>Serious Incident</td>
<td>Vandellos-1 (GGR), E, 1989</td>
</tr>
<tr>
<td>2</td>
<td>Incident</td>
<td></td>
</tr>
<tr>
<td>1</td>
<td>Anomaly</td>
<td></td>
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</tbody>
</table>

**Figure 4. The International Nuclear Event Scale**

Events in nuclear installations associated with the civil nuclear industry or events occurring during the transport of radioactive materials to and from these installations are classified at seven levels. The lower levels (1–3) are termed incidents and the upper levels (4–7) accidents. Events are classified according to their consequences on both the public and environment. Within the last two decades of nuclear power use for electricity production there have been two major accidents, with effects to the environment and the public opinion about nuclear energy, as described below.
On 28 March 1979 at the Three Mile Island Unit 2 (TMI–2) pressurized water reactor (PWR) in Pennsylvania, United States, an accident of INES Level 4 to 5 occurred, as shown in Figure 4. Although large fractions of the reactor core were severely damaged and redistributed in the reactor pressure vessel, no lower head failure occurred. There was an uncontrolled, but very limited release of radioactivity from the plant, consisting of noble gases to nearly 100%. Therefore, no external health risks for the plant staff or the public were indicated. As illustrated in Figure 5, there was only a small amount of operation experience with light water reactors (PWR, BWR) of about 1000 reactor–years at that time, compared with the total number of 6000 reactor–years experience today.

![Graph showing accumulated reactor operation years]

Figure 5. Accumulated Number of Reactor Operation Years for Electricity Production [2]

On 26 April 1986 Unit 4 of the Chernobyl nuclear power plant in the former Soviet Republic Ukraine exploded due to a criticality accident which was caused by an insufficiently planned—and under disregard for 5 major safety rules performed reactor experiment. During this accident of INES Level 7 (Figure 4) large fractions of the radioactive inventory of the reactor were released to the environment. The reactor type (RBMK), based on a graphite moderated, boiling light water cooled reactor core, designed and built in the former Soviet Union only, however, is not typical of nuclear power use for electricity production as there were merely 15 units with an operation experience of about 150 reactor years at that time and still just 17 units with 250 reactor–years experience today.

To date, the total number of reactor–operation–years has accumulated up to 8000 years. Thus, there is a large amount of experience with nuclear power plant operation and since the 1980's experiences are continuously discussed in an international forum (e.g. WANO) for increased nuclear power plant safety and performance in order to acquire a strong position in the electricity generating sector as a mature technology.

Another important aspect of nuclear energy utilization is related to environmental effects. Nuclear power is one of the most important emission free energy options. This may be an important part of a strategy to cut back global emissions of carbon dioxide (CO₂) which have
been discussed late last year at the Kyoto Global Warming Conference. If all nuclear power plants were replaced by fossil-fired power plants, basically assuming that a fossil-fired plant emits about 1 kg CO₂ per 1 kWh electricity, the CO₂ emissions of the whole energy sector worldwide would be increased by about 10%. Year by year in the 1990s use of nuclear energy avoided some 2.3 billion tonnes of CO₂ emissions per year, as depicted in Figure 3 above. The accumulated CO₂ emissions avoided during forty years of nuclear power utilization for electricity production have reached 35 billion tonnes, as indicated in Figure 6, corresponding to the worldwide anthropogenic CO₂ emissions of one and a half years.

Figure 6. Accumulated Worldwide Nuclear Electricity Production and CO₂ Emissions Avoided [2]

Furthermore, nuclear power also prevents from emissions of millions of tonnes of other pollutants like sulphur, nitrogen oxides and various carbon–hydrogen–compounds. Although alternative approaches to the global CO₂ reducing goal must be considered, nuclear energy represents an economical and promising base for a long term, sustainable development.

Summarizing, the historical development of nuclear power plant technology utilization for commercial electricity production during the past four decades and its present status quo show that nuclear energy provides an important contribution to the human energy supply, on the one hand, and represents a reliable option for a reduction of global anthropogenic CO₂ emissions, on the other hand.

Regarding safety improvements for existing nuclear power plants, the TMI–2 accident is of interest because of the present commercial dominance of light water reactors. This accident demonstrated that the nuclear safety philosophy evolved over the years has to cover accident sequences involving massive core melt progression and effects of man–machine–interactions in order to develop reliable mitigation strategies for both existing and advanced reactors. Last but not least, if the accumulated worldwide nuclear electricity production at the time of TMI–2 is related to the current one (see Figure 6), the resulting fraction of about 9% indicates that this accident took place at the very beginning of LWR operation and that a large amount of additional experience has been gained in the meantime within the two decades after TMI–2.
Key Phenomena of Late Phase Core Melt Progression

The relocation of core materials following a prolonged loss of coolant— and core heatup sequence during a severe accident in a light water reactor (LWR) marks an important step in the progression of core degradation. Once metallic melt is formed inside of fuel— and absorber rods during the early phases of core melt progression, after cladding failure it makes its way into the lower core regions, eventually refreezing under formation of local blockages within the original core boundaries. While the rod—like core configuration remains more or less intact during these early phases, with core material redistributions principally being limited to localized core regions and mainly metallic core materials, the later phases are characterized by significant redistributions of predominantly ceramic core materials in extended core regions involving porous debris bed—, molten pool— and cavity formation phenomena. The gradual loss of the original rod—like geometry in favour of ceramic—rich porous debris bed configurations resting on top of previously relocated core materials has been firmly established through the performance of severe fuel damage (SFD) experiments. There is a large degree of agreement in the international nuclear safety research community that oxidic remnants observed to rest in rod—like endstate configurations of fuel pellets and oxidized cladding shards in the upper parts of small scale bundle experiments would not remain stable in the scale of a full sized reactor core and that reflood induced fragmentation or thermal shattering events would accelerate redistributions of such core materials.

Research in this safety area was stimulated by the TMI—2 accident, which has furnished a great deal of significant information on late phase core melt progression beyond the onset of substantial ceramic material relocation [3]. As demonstrated clearly by this accident, continued heatup of debris bed configurations may lead to molten pools of principally ceramic core materials retained by ceramic crusts that gradually consume the surrounding structure, while advancing axially and radially towards the core boundaries. There is evidence from experimental observation that underlying blockages of relocated metallic materials have very little influence on the migration and growth of such molten pools, and may drain away from the advancing ceramic crust, depending on the coolant conditions, heat loss characteristics as well as axial— and radial decay power profiles within the core.

Release of the molten pool inventory is generally expected to occur when the ceramic crust reaches a core boundary where there is insufficient material upon which to refreeze, possibly leading to crust failure and massive melt relocation from the original core boundaries via different pathways onto the lower head. Depending on the crust failure size and location, the corium mass, composition and temperature as well as the types and thermal states of core structures involved, chemical interactions and surface erosion— or crust formation processes may influence the extent and timing of relocation. The arrival conditions of core material at the lower head are also affected by the fluid dynamics of molten corium when it passes through the lower core support assembly plates. Depending on the corium discharge rate from the original core boundary and the flow hole sizes of the different plates, relocating melt may be intercepted/collected by individual plates. As the local depth of a molten corium layer on a particular plate builds up, corium may spread horizontally and drain through nearby flow holes. Thus, corium collected by a particular plate as a single pour stream may drain from this plate in the form of several streams. The formation of multiple streams in this manner can
enhance melt breakup and modify the stream diameter as well as the surface area for heat- and mass transfer (e.g. steam- and hydrogen generation or fission product release).

The most likely mode of molten corium entry into the lower head region is therefore often envisioned in light water reactor safety analysis to be of multiple small diameter pour streams or cylindrical jets which, in case of wet core conditions, tend to further break up, disperse and quench due to interactions with the lower head coolant inventory. As a molten corium stream penetrates through liquid coolant, it will progressively break up into droplets due to hydrodynamic instabilities arising from interactions with the surrounding liquid. The droplets formed by the breakup process loose heat as they fall through the coolant and may freeze to become solid particles or accumulate as a non–particulate continuous layer of molten corium on the lower head. Depending on the extent of jet disintegration, potential for lower head failure can be brought about by jet impingement induced surface erosion and heatup, pressure loads due to rapid steam generation or localized decay heating from corium resting on the lower head. The timing and routes of molten corium arriving at the lower head and the melt characteristics, e.g. composition or temperature, thus need to be known for lower head failure potential analyses and developments of accident management strategies.

While the most important information on late phase core melt progression has come from post–accident examination of the endstate configuration of the TMI–2 reactor core and metallographic analysis of the core debris, considerable information on some boundary conditions and parameters during the different accident sequences was also obtained by broad and often indirect analyses, using the system measurements that existed. The core endstate configuration just after the accident, as determined by closed–circuit television and mechanical probing operations, is illustrated in Figure 7. A large cavity was found at the top of the core, extending nearly across the full core diameter [4]. The cavity was surrounded by standing fuel rods of varying damage all around the core periphery. Highly localized melt ablation damage was observed at the upper grid on top of the cavity that did not progress significantly higher and was probably caused by hot gas flow from the core and in–vessel natural circulation [3]. The other upper plenum structures and hot leg nozzles were found to be essentially intact.

Beneath the cavity a loose core debris bed rested on top of previously molten core materials. The loose particulate debris, 3 to 10% of which was less than 1 mm in diameter [5], included a mixture of fuel pieces, cladding shards, foamy/porous previously molten fuel, and structural– or control materials [6]. It is believed that this loose debris was formed during an effort to cool the core at 174 minutes into the accident. Nearly 30 m³ of water were injected into the core in less than 15 seconds, probably leading to thermal shock induced shattering of the oxidized and highly embrittled fuel rod remnants in the upper core regions.

Metallographic examination of debris particles indicated that most of the core debris bed mass remained at temperatures below 2000 K or was exposed to higher temperatures only for short times. Part of the debris was found to contain some particles of previously once-molten U–Zr–O mixture, indicating peak temperatures greater than 2200 K, and a few particles were once–molten (U,Zr)O₂ and UO₂, with peak temperatures between about 2800 and 3100 K [7].
Figure 7. Endstate Configuration of the TMI-2 Reactor Core [4]

The loose core debris bed was surrounded by both fuel rod remnants at the upper level and a horseshoe-shaped ring of consolidated core material at the lower level. It extended into the upper core support assembly\(^1\) (upper CSA) through the horseshoe ring opening at the east side of the core and a melted hole in the vertical baffle plates. The horseshoe-shaped ring extended upward to the third core former plate, indicating the volume of the core that was probably flooded with liquefied core material, before massive melt relocation took place, which is believed to have caused the central sinkhole in the loose debris bed, as inferred from core bore data [8] and illustrated in Figure 8. An additional peripheral sinkhole was detected in the east quadrant of the reactor core where the upper crust contour in the O-, P- and R–6 fuel rod assemblies was found to lie below the projected bottom crust one, as deduced from core bore data, supporting evidence of a localized escape path for molten corium in this region [9].

\(^1\) The upper CSA consists of vertical baffle plates, horizontal core former plates and the core barrel.
Figure 8. Cross Section of the TMI–2 Reactor Core Through the 6th Fuel Rod Assembly Row [9]

The consolidated region of previously molten core materials beneath the upper loose core debris bed was found to be about 3 m in diameter, with a depth of approximately 1.5 m near the center and 0.25 m at the outer edges. The central part of this consolidated region was composed of a resolidified (U, Zr)O₂ ceramic that was laced with previously molten metallics in a large variety of chemical interaction stages. This central region was additionally surrounded by a crust of previously molten ceramics and metals that had obviously not oxidized [10]. It is believed that the central part of the consolidated region had previously been in a molten state due to surface heat transfer from such kind of large-volume core materials agglomeration being too inefficient to compensate for the internal fission product decay heat production. It is likely that densification of this region, as the molten pool grew, contributed to the formation of the cavity observed at the top of the core (see Figure 7) [3].

The upper crust was composed of a ceramic phase with up to 25% metallic inclusions [6], with some embedded debris indicating interactions between the loose core debris and previously liquefied material [11]. Its thickness varied from 0.01 to 0.03 m. While no fuel geometry was retained in the upper crust, the bowl–shaped lower crust had a thickness of about 0.1 m and was composed of resolidified metallics including small ceramic inclusions, frozen in the flow channel between fuel rods. Standing columns of fuel pellets were all that remained of these fuel rods, devoid of cladding that had melted away and been incorporated into the resolidified metallic structure [4]. A lower crust sample from the core centerline is shown in Figure 9.

The molten metallics had dissolved in the cladding, and the melt mixture flowed into the cracks between the fuel pellets and completely filled voids in the ceramic fuel. Consequently, the metallics were still of adequately low viscosity to flow into the fuel pellets through the cracks and to form these metallic inclusions. Based on these findings, it has been suggested that the lower crust was formed by the flow of molten metallic material from the upper regions of the core to cooler areas near the water level in the lower core, where it cooled.
The lowest peak temperature of this metallic material was in a temperature range between about 1220 and 1570 K based on the formation of zirconium–rich eutectics with iron or nickel and chromium [6]. Thermal analysis has indicated that the upper inside surface of this lower (principally metallic) crust must also have had an insulating layer of frozen ceramic between it and the resolidified mass when that mass was molten, preventing the underlying metallic crust from remelting and renewed relocation. However, it was not possible to distinguish, after the accident, an inner crust ceramic layer from the resolidified mass [12].

The lower crust rested on fuel rod stubs, which varied in length from 0.2 to 1.5 m and extended upward from the lower grid to the bottom of the resolidified mass. On the east side of the core four adjacent fuel assemblies were found to be completely replaced with resolidified core material (see Figure 7), indicating that molten core material had drained into this region [4]. Post–accident inspections during the defuelling revealed that the primary relocation pathway of molten corium had obviously been through a large melted hole of about 1.5 m height and 0.6 m width in the baffle plates adjacent to these fuel assemblies, as portrayed in Figure 10. Three baffle plates and core former plates were melted through, and corium appeared to have flowed predominantly through this damage zone into the upper CSA. Regarding a likely scenario of molten pool failure and corium relocation which could have led to this baffle plate melt–through, it has been suggested in [13] that molten corium initially must have streamed laterally out of a side breach in the upper part of the crust surrounding the pool and flowed initially into the adjacent fuel rod assemblies, as illustrated in Figure 11 (a) and (b). In general, an intact fuel rod assembly between the crust breach and the vertical baffle plates would initially tend to break up and dissipate the corium stream, with subsequent gravity–driven corium drainage into the available open spaces of the coolant channels and rapid resolidification in lower core regions. However, after some time of continued corium discharge from the molten pool, the fuel assembly would have been molten through, which would have led to lateral impingement of a melt stream onto the baffle plates, as shown in Figure 11 (c).
Figure 10. Sketch of Post-Accident View on Baffle Plate Melt-Through [12]

This suggestion of gravity-driven, axial relocation prior to jet impingement was corroborated by the post-accident state of one peripheral fuel rod assembly directly in front of the baffle plate damage zone, which was found to be completely melted through at one point, with its lower portion full of resolidified corium [13]. Although resolidified core debris was also found in some other peripheral fuel rod assemblies adjacent to the baffle plate damage zone (see Figure 7), suggesting that part of corium had drained into them prior to the baffle plate failure, there was no hint of a flow path from these assemblies to the lower head. The principal relocation pathway was therefore identified in [5] as being via the baffle plate melt-through, upper- and lower CSA structures onto the lower head. It is believed that the melted hole in the baffle plates was initially small, but that it was enlarged subsequently by melt ablation during corium streaming through the breach [12]. Visual inspection of the core barrel indicated one local surface ablation damage area of about 100 mm in diameter which was probably caused by an impinging jet immediately after baffle plate failure [12]. The melt drained subsequently via flow holes in the core former plates towards the lower CSA, as illustrated in Figure 11 (d).
Figure 11. Hypothesized Scenario of TMI–2 Molten Pool Failure and Corium Relocation

Regarding the sudden discharge of corium from the molten pool over the edge of its lower crust, it was suggested in [14] that the discharge rate was not only driven by gravity but was accelerated by the upper crust collapsing under the weight of core debris resting on top of it. This collapse would have led to rapid corium displacement by the settling core debris and would also nicely explain the central sinkhole in the upper debris bed according to Figure 8. It is very difficult to trace the exact path the corium took as it moved through the upper CSA. Relocation analysis has indicated an initial lateral melt migration along the horizontal surfaces of the core former plates, with small diameter pour streams draining subsequently through the numerous flow holes in these plates, as sketched in Figure 12.
Figure 12. Sketch of Postulated TMI–2 Molten Corium Flow Through the Upper TSA

The multiple small diameter pour streams released from the numerous flow holes in the core former plates underwent partial breakup when flowing through the upper CSA under coolant reflood conditions, leaving behind crusts and fragmented core debris. Post-accident probing indicated the presence of about 4.2 metric tonnes of resolidified corium debris inside the upper CSA in different forms. Estimated profiles and locations of resolidified corium debris that completely encircled the upper CSA is shown in Figure 13 in an unfolded view.

Fragmented granular corium debris characterized by equiaxed grains varying from sand to gravel size [8] was found at various elevations. These ranged up to 0.8 m above the baffle plate melt-through, indicating that part of the molten material was rapidly cooled down, on the one hand, and that forces other than gravity may have influenced the material movement in this region on the other hand. The upward relocation may have been caused by either a manometer effect from the reservoir of molten corium in the core central region above the baffle plate melt-through during the major relocation event or from hydraulic levitation in later stages of the accident when the coolant pumps were restarted [8].

In some regions the fragmented granular corium debris rested on top of impenetrable corium formations\(^2\) and in some other regions it was found to lie directly on top of the core former plates. Crusts of resolidified corium debris were also detected on the structural surfaces of the lower core former plates and the core barrel, with thicknesses varying from 5 to 50 mm [13].

\(^2\) A manually operated spud was used to determine the corium penetrability in this region [8].
The flow holes in the baffle plates just below the core former plate 6 according to Figure 13 were observed to have experienced minor ablation damage adjacent to the baffle plate melt-through. Advancing around the core farther from this damage zone, the observed conditions were found to change from slightly ablated flow holes to undamaged ones, followed by partly and finally completely plugged flow holes on the opposite side of the core. This indicates that very nearly the entire volume between core former plates 6 and 7 must have filled at a rapid rate with molten corium for most of the way around the core, which is equivalent to about 7.8 metric tonnes of corium [13]. The majority of the molten corium subsequently drained out of this region through the flow holes in the core former plate 7, leaving behind crusts of varying thicknesses on both the horizontal and vertical structural surfaces. The presence of resolidified corium plugging some of the flow holes in the lower core former plates indicates that molten corium probably drained through them before. It is likely that once a flow hole was plugged, any subsequent corium that flowed in that area was diverted by the plug and drained through an adjacent hole [5]. Thermal analysis has confirmed this suggestion and shown that once a flow hole was plugged, there would only be the remaining superheat in the overlying molten corium to remelt this plug. However, the adjacent structural materials would melt first, which is inconsistent with the observed conditions showing a significant amount of debris fused to core former plates, but no significant surface ablation damage [13]. Apart from the resolidified corium debris that was found in the upper CSA, approximately 5.8 metric tonnes were retained in the lower CSA[3][4]. Resolidified corium was found in a number of flow holes all around the perimeter of the lower grid, corresponding to melt relocation through the upper CSA. There were also indications that part of the corium had melted back into the active core region in the east quadrant near the level of core former plate 8 at the base of the fuel rod stack, with subsequent relocation into the lower CSA. Once in this region, the majority of the molten corium continued to move immediately vertically downward through the flow holes of the different plates. However, visual examination revealed resolidified corium not only in or above the flow holes of the different lower CSA plates in the east core quadrant beneath the baffle plate melt-through but also at several locations all around the core, indicating that part of the molten corium obviously had flowed around the perimeter of these structures [5]. Many of the plugged flow holes were found to line up quite well, which indicates that the corium flow moved vertically downward and covered much of the peripheral regions, causing the corium to relocate from the elliptical flow distributor at several locations, as illustrated in Figure 14.

The limited damage on the elliptical flow distributor suggests that the first corium that reached this plate and subsequently relocated onto the lower head at several locations was probably relatively cool, as a result of the heat loss to the coolant and to the melting of some structural material in the upper CSA. It should be noted that nothing in recorded data or post-accident core conditions suggests that an energetic steam explosion occurred as the molten corium relocated through the upper CSA with the vessel essentially full of water [3].

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3) The lower CSA consists of five perforated horizontal plates, i.e. the lower grid, the flat flow distributor, the lower grid forging, the instrument support plate and the elliptical flow distributor, as shown in Figure 14, that span across all or a significant fraction of the core width, support the weight of the reactor core and distribute the coolant flow under normal reactor operating conditions.
Figure 14. Postulated Scenario of TMI–2 Corium Relocation onto the Lower Head

It is thought that the reactor coolant system pressure of about 12 MPa was too high at that time for steam explosion triggering. However, both the reactor coolant system pressure and the cold-leg temperatures increased rapidly as a result of the heavy non-explosive steam production caused by heat transfer from the molten corium to the water. Analysis of the primary coolant system pressure pulse indicated rapid steam generation leading to a pressure spike of 2 MPa above the 12 MPa ambient pressure for at least 15 minutes.

It is likely that the first corium reaching the lower head cooled down rapidly under formation of a cup–shaped crust of variable thickness that insulated the lower head and many of the nozzles from extensive thermal damage. This suggestion is based on post-accident examinations, showing some nozzles in the lower head central region to be so severely damaged that only small stubs remained, while other nozzles were found to be discolored up to elevations of 0.27 m above the lower head, where they were ablated or completely melted off [5]. Furthermore, the fact that some severely damaged nozzles were found in close proximity to others which were only slightly thermally attacked or discolored confirms the suggestion that the thickness of the insulating crust was not uniform and the movement of molten corium across the lower head was multi-directional and not one massive flow. As further corium drained through the different flow holes in the elliptical flow distributor plate, the insulating cup–shaped crust that had initially formed on the lower head was filled.
It was estimated that 12400 kg of loose core debris and 6700 kg of agglomerated core debris rested on the lower head. Video examinations of the lower plenum prior to the core bore operations indicated that resolidified core debris had accumulated to a depth of about 0.75 to 1 m above the lower head wall. The debris extended radially to the downcomer, except in the north quadrant of the lower head, where a high steep face of rock was detected at about 1 m from the core centerline. Visual observations of the lower loose core debris bed indicated the particle size to vary widely, ranging from granular debris less than a few millimeters in diameter near the center to larger rock-like formations ten to thirty centimeters across near the lower head wall. Two types of core debris were identified. Lava-like formations of core debris, reproduced from video images in Figure 15 (a), were observed in the northern quadrant of the reactor pressure vessel, while rock-like particulate core debris covered the lower head in the southern quadrant, as shown in Figure 15 (b).

Figure 15. Video Images of Resolidified Core Material from the Lower Debris Bed [4]

Metallographic examination of particulate core debris samples indicated microstructures typical of cast multicomponent materials, i.e. rounded grains with oxidic phases in the grain boundaries. The primary grains generally appeared to be a single-phase material, as would be expected if a $\text{UO}_2$-$\text{ZrO}_2$ mixture was rapidly cooled from the liquid state.

However, portions of these grains showed indications of segregation into two distinct phases, which is typical of a slower cooling rate for part of the material, and in some areas the whole primary grain structure was found to be segregated into these two phases. Moreover, apart from the primary uranium-zirconia grains, significant amounts of eutectic iron-, chromium-, nickel-, and aluminium oxides were detected at the primary grain boundaries next to large voids, possibly indicating much lower solidus temperatures than that of one of the bulk (U,Zr)O$_2$ material. It was therefore suspected in [15] that the grain boundaries could have remained liquid after the grains themselves had already resolidified, which would have allowed portions of the debris to remain mobile at temperatures near the melting point of stainless steel (1670 K) which is far below the bulk (U,Zr)O$_2$ solidus temperature (> 2500 K).

As part of the defuelling efforts, the loose core debris resting upon the reactor pressure vessel lower head was completely removed, revealing a variable surface topography of an extremely hard and monolithic debris layer. Virtually no adherence of this layer to the lower head could be detected during the defuelling. Numerous layer samples were examined and found to be fairly homogeneous with relatively small variations in composition and density. Because of the
similarity of the composition of the ceramic debris in the lower plenum to that of the material in the central molten core region, i.e. primarily previously molten (U,Zr)O₂, it was suggested that the material in the lower plenum came from the central core region [5]. In most of the samples interconnected porosity was observed, probably caused by bubbling of steam or structural material vapours, implying that at least part of the debris layer must have remained molten on the lower head for a sufficient time in order to allow bubble formation and coalescence to occur. Moreover, the presence of urania- and zirconia-rich (U,Zr)O₂ phases in most samples indicated a gradual cooldown of the debris layer rather than a rapid quench. Regarding the extent of possible damage to the lower head due to thermal- and chemical attack by relocated core debris, material samples were extracted from the lower head and examined metallographically. Based on these examinations, interactions between the core debris and the lower head wall were found to be limited to thermal interactions, as a result of the chemical stability of the core materials involved. Moreover, a local hot spot was identified, as illustrated in Figure 16, covering a nearly elliptical region of about 1 m width in the north–south– and of about 0.8 m width in the east–west direction.

![Diagram showing the location of the hot spot region](image)

**Figure 16. Location of Hot Spot Region on the TMI–2 Lower Head [5]**

The hot spot location was consistent with the lower head region where the most severe nozzle damage had occurred, confirming the suggestion of an insufficient basal crust layer thickness to adequately insulate the lower head from a sustained heat load by the overlying core debris. A peak temperature of about 1373 K was reached at the hot spot central region (near the interface of the vessel base material with the overlying cladding) for a period as long as 30 minutes prior to rapid cooldown and of about 1073 K at the hot spot periphery. At some distance from the hot spot, however, the ferrite–austenite transformation temperature of about 1000 K was not exceeded, as indicated by metallographic examination of lower head samples.
The rapid cooldown of the hot spot region after about 30 minutes was probably associated by a gap formation between the core debris and the lower head wall through which liquid coolant flow could have been established. Thermal analysis revealed that a considerable period of time of up to 30 minutes would be required to cool down the peripheral debris regions before liquid coolant would be able to penetrate in the gap towards the hot spot region. Corresponding calculations indicated that coolant travelling through a negligible volume of interconnected flow channels within the core debris, i.e. much less than 1% of the core debris volume, and that a very small gap thickness, e.g. as small as 1 mm, would provide sufficient cooling to prevent lower head failure.

Summarizing, the TMI–2 accident has provided significant information on late phase core melt progression and initiated a number of reinforced research efforts worldwide since 1979. Regarding the generality and applicability of the core materials behaviour observed in TMI–2, e.g. recent experiments carried out in the PHEBUS test facility have confirmed the formation of a ceramic molten pool similar to that one detected after the accident in the TMI–2 core [16]. The PHEBUS programme is the centre piece of an international cooperation investigating core melt progression as well as physical– and chemical fission product behaviour during a severe accident [17]. The PHEBUS facility offers the opportunity to check through a series of integral in–pile experiments that all the key phenomena governing core degradation from the early phase of cladding oxidation and hydrogen production up to the late phase of core melt progression are correctly understood and properly modelled in those computer codes used to assess the safety of LWR plants and the efficiency of accident management measures.

Two experiments have been performed up to now: PHEBUS FPT0 and FPT1 [17]. Post–test examinations of the PHEBUS FPT0 and FPT1 bundles included a large number of computer tomographic radiographs revealing an advanced stage of late phase core melt progression, as shown in Figure 17. Only 50% (FPT0) up to 70% (FPT1) of the fuel remained in rod–like form. The remainder was dissolved or molten during the tests. Between 2 and 3 kg of once liquid material accumulated on and below the lower grid in FPT0; slightly less in FPT1. Intact rods were only recognized at the bottom of the bundles where silver–rich metallics from the absorber rod had accumulated [17]. The third PHEBUS FPT4 experiment, scheduled for 1999, will focus on low volatile fission product release from a debris bed of burnup fuel and oxidized cladding fragments which is heated up to partial melting [17].

Apart from the PHEBUS programme, various research projects have been launched within the European Framework Programmes on Nuclear Fission Safety [18, 19], concentrating on the various important aspects of LWR safety. RUB/NES, for example, is currently involved in a number of projects investigating core degradation, fission product release from molten pools, fission product release and speciation as well as thermochemical modelling and data [19].

Regarding further lessons of late phase core melt progression learnt from the TMI–2 accident, for example, the key processes governing the quenching and early cooling of molten corium slumping into the liquid coolant inventory of the lower head region are also investigated. These processes determine the quenching potential, the thermal–mechanical loads on the reactor pressure vessel as well as long term core debris coolability and retention in–vessel, which — as demonstrated by the TMI–2 accident — is possible, provided an early vessel failure does not occur [19].
Figure 17. Pre– and Post–Test Appearances of PHEBUS FPT0 and FPT1 Bundles [16]

The FARO plant, as an example, is one of the most important large multi–purpose facilities concerned with core melt quenching in the reactor pressure vessel lower head and in–vessel steam explosion. Up to 200 kg of UO₂–based melts at 3000°C may be generated and used for melt–water– as well as melt–structure–interaction studies. Results of FARO tests carried out so far have indicated, for example, melt fractions of 15% to reach the bottom plate in a molten state, mean fragment sizes of a few millimeters and production of significant amounts of hydrogen of the order of 0.3 kg for 150 kg of melt [20], which is relevant for the hydrogen source term, since quenching is generally considered as the worst–case scenario which should not exceed safety–critical values. Regarding the TMI–2 accident, a total hydrogen mass of about 459 kg was produced by partial oxidation processes of the metallic core and structural materials, with large uncertainty concerning the extent of oxidation during the different accident sequences. In this respect, e.g. the current FZK quench tests are of interest [21], investigating reflood induced hydrogen production due to formation and propagation of cracks through coherent protective oxide scales. Although molten pool– or debris bed configurations are out of the scope of these tests, they will help to improve the basic knowledge of the main processes governing the transition from rod–like geometries to debris bed configurations.
Accident Management Strategies

Regarding operator interventions to prevent early phase damage progression within the original core boundaries, water injection into the vessel (bleed and feed strategy) is still recognized as an important, generally reliable measure for all cases in which insufficient knowledge on the extent and sequences of various phenomena and core materials chemical interactions as well as suitable online information about the reactor conditions to identify both the location and extent of core melting is not possible with sufficient reliability.

The feasibility of such operator intervention strongly depends on the delay to restore failed emergency core cooling systems or to provide specific alternative measures to inject water into the overheated core. The efficiency of an operable emergency core cooling system, however, may not be guaranteed, depending on the extent of core damage and the thermodynamic state of the coolant system.

Even in case of successful water injection, significant amounts of hydrogen may be generated by quench induced oxidation processes, increasing the containment loadings by hydrogen combustion, on the one hand, and possibly initiating renewed heatup, melting and relocation of core materials, on the other one.

Last but not least, depending on the presence of high temperature melts at the time of reflood, fuel coolant interactions of various strength may give rise to pressure peaks well into the MPa range and rather significant thermal-to-mechanical energy conversion threatening the integrity of the reactor pressure vessel. This would jeopardize a severe accident management strategy that aims to contain sustainably core material in the reactor pressure vessel and to limit the radioactive consequences out of the containment [22].

A second, effective in-vessel retention concept for the later phase of core damage progression beyond the onset of substantial ceramic material relocation is to cool the reactor pressure vessel — and hence core debris internally resting on the lower head — from the outside by simply immersing it in water. Its success depends on the accommodation of the heat fluxes imposed internally by core melt on the reactor pressure vessel wall while maintaining e.g. nucleate boiling on the outside. Such external flooding can be assured by proper containment conditions and the availability of sufficient quantities of water for the required times.

The feasibility of such a concept is plant dependent, as far as the reactor pressure vessel thermal insulation and cavity design are concerned. However, the efficiency of external flooding is yet to be assessed, particularly for large reactor pressure vessels, high volumetric heating rates and late flooding, and has to be further investigated to explore the feasibility of in-vessel core retention and coolability as one of the main objectives of this meeting.

Severe Fuel Damage Code Modeling

Leading SFD codes worldwide, e.g. ATHLET–CD, ICARE2, MELCOR or SCDAP/RELAP5, are being applied and developed to assist in predicting the overall behaviour of a reactor core during a severe accident. The accuracy of code predictions is generally being improved and extended on an international level of cooperation by validation calculations of various experiments and benchmark activities, on the one hand, and by critical review, comparative assessment and improvement of the modeling basis of individual codes, on the other hand.
Emphasis of these efforts is put on a reliable application of SFD codes to plant performance as well as reactor safety analysis to help to define advanced severe accident management concepts for both existing and future reactors. In general, the confidence of SFD code predictions decreases with increasing extent of core damage progression. The status quo of SFD code models for the early phase core damage progression is similar for all these codes which show a fairly good predictive capability of hydrogen production and core behaviour up to the formation of metallic blockages. Concerning relevant models for late phase core damage progression, however, the capabilities of the different codes differ substantially. The following table provides an overview about the status quo of late phase models addressing debris bed— and molten pool modeling in the core region.

The ATHLET–CD Code [23] is being developed by the Gesellschaft für Reaktorsicherheit (GRS) in cooperation with the Institut für Kernenergetik und Energiesysteme (IKE) in Stuttgart for analysis of severe accidents involving core degradation phenomena. The core degradation part of ATHLET–CD is based on selected models of the KESS–III Code developed at IKE [24]. The current official version 1.1D/0.2E of the ATHLET–CD Code is able to predict core damage progression up to the formation, relocation and solidification of ceramic melts. At present, the MESOCO–2D (Melting and Solidification of Core Material) Module developed at IKE [25], is being implemented into the ATHLET–CD Code. MESOCO–2D is a two–dimensional code describing the behaviour of a porous dry debris bed, the melting and solidification of debris particles, molten pool formation and behaviour including the formation, growth and failure of peripheral crusts. Furthermore, the WABE–2D (Water Bed) Module is also under development at IKE, describing the behaviour of wet debris beds in both the core region and the RPV lower head plenum. It will be implemented into the ATHLET–CD Code in order to model the debris bed behaviour within the core region. At present, a mechanistic model simulating melt relocation from a molten pool in the central core region onto the lower head is not available.

Regarding the behaviour of debris beds resting on the RPV lower head, two options are conceivable. Firstly, a combination of MESOCO–2D and WABE–2D called MEWA–2D could be implemented into the ATHLET–CD Code. While MESOCO–2D provides information about the dry debris bed behaviour, WABE–2D is a module calculating the dryout of flooded debris beds as well as quenching. Secondly, a model called AIDA could be incorporated into the ATHLET–CD Code to describe the behaviour of molten pools submerged by water in the lower head plenum.

In the current ICARE2 V2 Mod 2.3 Code version [26] a two–dimensional, finite volume module for calculating debris bed behaviour is implemented. However, the module does not provide an automatic transition between rod– and debris bed geometries. Therefore, a predefined dry debris bed, specified by the code user, represents the starting point for the calculation.

Fuel and control rod structures are allowed to be embedded in the debris bed. The conduction in the solid, liquid and gas phase as well as radiation between particles are treated. In addition, heat transfer between different phases is calculated by the module. Furthermore, the conductive heat exchange between debris particles and embedded structures like rods, shrouds and plates can be simulated.

Regarding melt relocation, both melt flow through the porous debris bed and falling through void regions are calculated. The formation of a molten pool is considered, except for the mass transfer inside the pool due to natural convection. The increased heat transfer due to natural
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convection is assessed by classical Nusselt number correlations. Crust formation processes on cold structural surfaces are treated.

In the integral code MELCOR 1.8.4 [27] debris bed formation and behaviour in the core region are calculated in the "Core (COR) Package". These models are highly parametric, allowing a great user influence on the code. Regarding the core region, the models calculate formation and behaviour of nonporous and porous debris. For calculating the solid debris formation, several very simple mechanisms are implemented. Furthermore, the pool formation and behaviour is calculated. Regarding the lower RPV head plenum, two different modules are available in the MELCOR Code. Generally, these models were developed to describe especially BWR structures. But they are also applicable to PWR geometries. The very simplified "Lower Head (LH) Model" of the "COR Package" is able to simulate debris beds under wet conditions. Heat transfer from the debris particles to the lower head is modeled parametrically using user specified heat transfer coefficients, heat transfer areas and masses. Convection heat transfer rates from the particles and lower head to the fluid are also modelled using the core region heat transfer models.

The failure of the lower RPV head is assumed to occur whenever a failure temperature specified by the user is reached. The more detailed lower head model of the "Bottom Head (BH) Package" is applicable to dry debris beds. Conductive heat transfer inside the bed and through control volume-to-wall structures is calculated. Additionally, radiation and convection from the upper debris bed surface to both the vessel atmosphere and intact structures above the bed are considered. Furthermore, metal-steam reactions inside the debris bed are calculated for Zircaloy and stainless steel. Melting and relocation of pure species and eutectic mixtures within the bed are included into the model. Regarding the RPV lower head failure, a model for bottom head creep rupture is available.

Compared with the above mentioned codes, the SCDAP/RELAP5 Code Version MOD3.1 [28] provides most detailed late phase modeling. Regarding the core region, the code calculates the formation of nonporous debris due to relocation and solidification of metallic melt. The temperature variation in the axial direction of the nonporous debris is calculated. Furthermore, the heatup and melting, the transfer of heat by convection and radiation from the bottom and top surfaces and the internal heat generation of the embedded fuel rods are taken into account. The formation of porous debris beds due to embrittled and fragmented oxidized fuel rods is also calculated. The heat-up of porous debris is calculated and the convective heat transfer between debris particles and the fluid is considered, but not the radiative heat transfer from debris particles. As internal heat source only the fission product decay heat released within the particles is considered; particle oxidation heat is not taken into account. Three states of the debris are possible, namely debris in state of dryout, in the quenching process and in quenched state. The formation and heatup of a molten pool and the spreading of molten material is performed by the molten pool model. The heat fluxes on the inner crust surfaces are calculated using the Jahn and Reineke correlations [29]. The stability of the molten pool supporting crust is also modelled. These crusts are generally regarded as stable when the heat fluxes on the molten pool internal side of the crusts are lower than the heat fluxes on the outer crust sides, where radiation is considered for the heat flux calculation. After crust failure, the downward and lateral melt spreading and new crust formation are calculated.
Core melt can be discharged into the lower head plenum by three events: 1) Melting of the vertically oriented crust at the core periphery, 2) failure of the upper crust and 3) downward penetration of the molten pool and subsequent discharge after reaching the lower edge of the modeled core region. The failure criteria for the crusts are based on user specified thicknesses. The code user is able to specify whether the melt reacts with water during melt relocation into the lower RPV head plenum or not.

The debris bed behaviour in the lower RPV head plenum is calculated by a model based on the two-dimensional, finite element heat conduction code COUPLE. This model takes into account the decay heat and internal energy of debris particles. It also includes the modeling of spatially varying porosity, the thermal conductivity of porous debris material, a debris bed whose height grows discontinuously with time, dryout and quenching of the debris bed, radiation heat transfer in a porous material and melting or freezing of debris particles; oxidation of metallic particles and fission product release are not considered. Concerning the assessment of heat transfer due to natural convection inside a molten pool, correlations of Mayinger et al. [30] or Jahn and Reineke [29] can be used. For calculating the rate of heat transfer from a solid debris region into a structure in contact with the debris particles, the gap heat transfer coefficient is defined by user input. The RPV lower head failure is calculated by a creep rupture model. The failure depends on the structure temperature as well as on the effective stress in the structure.

**Remaining R&D Issues**

Leading SFD code models for late phase core behaviour may be subdivided into categories: Core materials resting in a certain configuration and core material redistributions from one into the next configuration. Relevant models of the first category, e.g. for debris bed heatup and melting are fairly well mechanistically based, while those of the second one, e.g. thermal shattering induced debris bed formation, are predominantly parametric.

Relevant SFD code models (MELCOR and SCDA/RELAP5) for the molten pool behaviour in the lower RPV head plenum are rather simplified and parametric at present. They do not consider effects of RPV wall creep caused by core debris decay heating, jet impingement induced erosion or chemical interaction induced ablation of the RPV wall. Moreover, formation of a gap resistance between core debris resting on the lower RPV head involving coolant penetration and boiling heat removal is not treated. Therefore, both additional theoretical and experimental work addressing the late phase behaviour in the lower RPV head plenum seems to be necessary in order to get a better understanding of the complex key phenomena of late phase core melt progression, on the one hand, and to provide a more extensive and suitable database for model development and improvement, on the other hand.

**Conclusions**

The value of safety related work cannot be well assessed quantitatively. However, direct and indirect effects of thorough safety related research work strongly contributed to the fact, that since the TMI–2 accident more than 30 000 billion kWh electricity could be produced by LWRs without any accident beyond INES Level 3. Furthermore — with some reservation regarding especially some eastern European units which are being upgraded — it is very unlikely that a high-level accident needs to be expected in years to come.
It can be concluded that the work on severe accident research has contributed substantially to the awareness of real risk potentials and realisation of consequent measures in the entire nuclear community, including operators at the first place, which lead to a high reduction of human and technical failure risks. In short, an important contribution to an enhanced safety culture covering all areas of nuclear operations can be quoted. But these activities have also to be continued with undiminished effort in order

- to keep or improve the high standard achieved to date,
- to further utilize possibilities provided by the constantly ongoing worldwide technical development, research tools and innovative improvements,
- to generate enhanced safety features and
- to constantly prove, demonstrate and convey to the public that nuclear energy is a very important, safe and reliable contribution for a worldwide sustainably growing electricity production.

This meeting gives experimentalists and model developers an excellent possibility to share and exchange information and to stimulate further research.

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IN-VESSEL RETENTION AS A SEVERE ACCIDENT MANAGEMENT STRATEGY

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ABSTRACT

A global view of the in-vessel retention idea, as rendered recently in DOE/ID-10460 and DOE/ID-10541, is provided, with updates on on-going confirmatory work on fundamental aspects of the problem, and projections about potential future applications of the technology. The author's point of view on assessment of complex problems and scaling is emphasized.

1. INTRODUCTION

In-vessel retention (IVR) is a severe accident management (SAM) strategy which, based on externally flooding the reactor vessel, is aimed at arresting the downward progress of an unconfined (unmitigated) core melt accident. Starting with Lovisa (Theofanous, 1989), and independently with the AP600 (Henry et al., 1991; Henry and Fauske, 1993), this concept has gained major and continuing international attention, and led to a plethora of research activities.

The principal attraction of such an approach is that, if successful, it would eliminate the whole ex-vessel sequence of highly complex, and potentially threatening to containment integrity, phenomena. The corollary attraction is that, if an adequately robust, for its success, case could be made, there would be no need to wrestle with the inherently more uncertain consequent evolution, especially concerning on-the-floor coolability. Such a “neat” case is not only efficient from the research and engineering standpoint, it is also very helpful in terms of improving risk perception attitudes (i.e., acceptance of nuclear power by the public). Moreover, preventing the spreading/ dispersion of such large amounts of radioactive materials would appear to be attractive for the post-accident tasks.

For Lovisa, IVR was made the cornerstone of the whole severe accident management strategy (Tuoministo and Theofanous, 1994). This was accepted by the Finnish regulatory authority (STUK), and implementation of the whole SAM scheme is essentially complete (Lundström et al., 1996). For the AP600 also, IVR constitutes a key element of SAM, and the certification review by the U.S. Nuclear Regulatory Commission (USNRC) is almost complete. The low power density and lack of lower head penetrations make these two cases very similar — they evolved in close relation to each other and form the basis for much of what is to be discussed here (Theofanous, et al., 1997a; Theofanous et al., 1996b; Kymäläinen et al., 1997). Presently, IVR is considered for possible implementation in several operating reactors, and for possible adoption in several advanced designs. These “other” applications require consideration of larger cores, higher power densities, and lower head penetrations.

Beyond its classical consideration at the pressure vessel boundary, the IVR technology has been suggested in further, both “upstream” and “downstream,” contexts. The “upstream” context would work in a situation like in the AP600, where the thick radial reflector and the low power density cause a significant delay in melting through the core barrel, and where, in the case of direct vessel injection (DVI) LOCA, cavity flooding would lead to internal vessel reflood, prior to melt relocation. Consequently, cooling of the core barrel would stabilize the accident at this stage (Theofanous et al., 1996a, Vol. 2). Clearly, a reactor cooling system can be engineered so that such a reflood behavior occurs passively, to follow any significant core degradation. In the “downstream” context, the IVR technology has been proposed (Theofanous, 1993) as an ex-vessel core catcher. Called, in this case, basement-internal thermal boundary (BITHERB), it (a lower-head like structure, supported underneath appropriately to allow sufficient space for coolant flow, while at the same time resisting any dynamic loads) was configured to be embedded in the 10-m-thick concrete basement (of the SBWR), and to come into play in case the core-on-the-floor failed to produce a stable, coolable state. Clearly, exposed or temporarily (weakly) covered configurations can be equally well selected, based on particular design conveniences. These “alternative” implementations of the IVR technology possess significant flexibility, and they deserve the most careful consideration.

The case for in-vessel retention consists of two major technical components, as summarized in Figure 1, which can be visualized with the help of Figure 2. Very simply, in the “Steam Explosion Regime,” we are looking for the integrity of the lower head under the dynamic loads that could arise from steam explosions, potentially occurring during the relocation of the melt from the core region to the lower plenum. In the “Thermal Regime,” we are looking for the integrity of the lower head under the natural convection thermal loads, from the molten pool, on the inside, and the cooling due to water boiling on the outside. In this case, significant wall melting could occur, and the thermally-induced stresses must also be considered. In the former case, the delivered local impulse is important, and integrity can be assessed by comparison to the impulse needed to produce failure (IP). In the latter case, the local heat flux is important, and integrity is assessed by comparison to the flux needed (q_r) locally, to produce departure from nucleate boiling. This is necessary and sufficient, because the the role of thermal stresses is subsumed by this mechanism (Theofanous et al., 1996a). In both cases, we can speak of generalized “loads” and “fragilities,” and it is essential that both of these be expressed in a manner that reflects uncertainties.
Fig. 1. The two major technical components of in-vessel retention \( q_w(\theta) \) and \( q_{CF}(\theta) \) are the local heat fluxes at the wall and the local critical heat flux on the outside, respectively; \( \delta_w(\theta) \) and \( \delta_{w0} \) are the local and the initial wall thickness, respectively; \( I \) is the local impulse at the wall due to a transient pressure history \( p(t, \theta) \), and subscript \( F \) defines a failure value.

Fig. 2a. Illustration of the "Steam Explosion Regime".

Fig. 2b. Illustration of the "Thermal Regime," in the AP600 cavity geometry.
For problems of this type, besides the usual uncertainties due to stochastic variability, we have also what is known as epistemic uncertainty, and for this, we need rather careful methodological treatment, including extensive review procedures to produce usable results (Theofanous, 1996). In particular, for the AP600 case, besides the USNRC review noted above, two independent international groups of 17 and 16 experts were engaged in the review of the two aspects of the IVR problem, respectively (Theofanous et al., 1996a; 1996b). All comments and responses are documented in these volumes, so that issues can be followed clearly to resolution. These interactions are helpful not only in providing a perspective of where the more “difficult” aspects of the problems lie, and in hopefully preventing the resurrection of settled questions, but also in allowing the reviewers’ own and synergistic contributions.

In this paper, I concentrate on the “Thermal Regime,” and include some more recent, fundamentally-oriented, confirmatory work that we have carried out at UCSB in the past few months. Similarly, we have made significant advancements in the assessment of the “Steam Explosion Regime,” especially including a new code, the PM-ALPHA-L (Eulerian-Lagrangian, 3D). These can be found in an addenda to DOE/ID-10541, 10503, and 10504 which are about to be published. Regarding the overall approach it will become apparent that I favor a synthesis based on key physics and fundamentals, even if they lead to complex computations, rather than the usual system-level code calculations. The presentation is structured and presented in the following order:

- Thermal Failure Criteria
- Melt Progression Considerations
- Loads Evaluation Methodology
- Quantification of Thermal Loads and Integrity Assessment
- Concluding Remarks

Finally, in closing these introductory remarks, I wish to make it clear that it is not among my objectives to include a literature survey here — this was done rather thoroughly in the main AP600 documents (DOE/ID-10460 and DOE/ID-10541), which also made use of the works of others, as appropriate.

2. THERMAL FAILURE CRITERIA

For a completely depressurized primary system (which is the case of interest for IVR), the thermal failure mechanism is boiling transition. The heat flux that causes such a transition is called critical heat flux (CHF), $q_{cr}$. Generally speaking boiling transition manifests itself by the formation of dry patches on the boiling surface, which causes rapid deterioration of heat transfer, complete dryout, and escalation of the surface temperature to a new level necessary to accommodate the heat flux under film boiling. Also generally speaking, the CHF is adversely affected by the presence of vapor, and this is especially interesting to the present configuration, with a large, nearly horizontal surface around the pole of the lower head (see Figure 2b). This is a situation peculiarly unique to IVR. As we get away from the pole, the situation looks increasingly more and more like normal two-phase flow. Unlike situations studied previously, however, here the outer boundary of the two-phase boundary layer is free, the phases within it are subject, depending on position, to various degrees of stratification, and both of these factors make the relation to known results impossible. Furthermore, complications also arise, due to instabilities in the global natural circulation flow, and the presence of subcooling (~12 °C caused by the gravity head).

For the AP600 assessment, the thermal failure criteria was based on direct, full-scale simulations in the ULP-2000 facility. The facility is illustrated in Figure 3, and the result of the simulations is shown in Figure 4. The line in this figure was drawn by (θ in degrees)

$$q_{cr}(θ) = 490 + 30.2θ - 8.88 \cdot 10^{-1}θ^2 + 1.35 \cdot 10^{-2}θ^3 - 6.65 \cdot 10^{-5}θ^4 \text{ kW/m}^2$$  \hspace{1cm} (1)

such that it bounds from below all data points. That this is a conservative bound was demonstrated by many more experiments (Theofanous et al., 1996a), including sensitivity studies to natural circulation flow rates (carried out by throttling the flow), a heater made out of reactor vessel steel covered with a typical baked-in paint used for protection, and the presence of baffles representing the reflective thermal insulation in the reactor. The other large-scale facility, the SULTAN in Grenoble, was designed for studying separate effects, and produced results that confirm those of ULP-2000 (Rouge, 1997).

Briefly, the design criteria for ULP-2000 were to match the heater surface length scale, shape, and thermal inertia, power shape, and natural circulation path height and hydraulic diameter. Thus we arrive at the full height (~6 m), "slice" geometry, shown in Figure 3. The heater element is made of three 7.6 cm-thick copper blocks, with a width of 15 cm, and an overall radius of curvature of ~2 m. The surface is aged and well-wetted, as is expected to be the case for the reactor. Heating, to a maximum flux of 2 MW/m², is obtained by cartridges imbedded in the copper blocks. They are, individually, under computer control, and the power shape is adjusted such as to properly represent any specified power shape (see below) in the axisymmetric reactor geometry. In its present version, the facility is equipped with a
Fig. 3. Schematic of the ULPU facility. Configuration III is obtained by installing a baffle, to simulate the reactor vessel thermal insulation.

Fig. 4. The coolability limits of a reactor pressure vessel lower head, from ULPU simulations. The data points at $\theta \sim 0^\circ$ are shown displaced to the right for clarity.

curved baffle, with a continuously adjustable positioning, aimed to focus on optimizing performance with respect to convective effects.

The side windows in the ULPU test section (see Figure 3) allow direct visualization of the boiling transition phenomenon, and hence an unprecedented identification of the mechanisms. This fortunate circumstance is a consequence of the inverted geometry and the ULPU slice design, but the mechanisms may also be relevant to normal boiling geometries, where such direct observations are not possible. In our most recent efforts, we use these observations, and related surface microthermocouple responses, together with theory, to better understand, predict, and ultimately influence critical heat flux (Angelini et al., 1997).

So far, this work has focused on the pole region ($\theta \sim 0^\circ$). In the absence of significant forces to remove the boiling bubbles rapidly enough, even under moderate heat fluxes, they coalesce to form something like a thick vapor film, or a "lens," which first grows and then slips away by growing radially, under the action of gravity, as illustrated in Figure 5. During this vapor, or lens, "residence time," the surface continues to be cooled by a thin liquid layer, left behind at the base of the growing bubbles. The initial thickness of this layer has been estimated at $\sim 100 \mu m$, so the time required for it to completely vaporize under a given heat flux can be readily estimated. Transition occurs if the liquid, sweeping behind the lens departure, arrives after this sort of dryout has occurred. How these two times come to intersect as we increase the heat flux, is illustrated in Figure 6. In this figure, taken from measurements in an ULPU test, the "depletion time" refers to the time required to completely vaporize the liquid film, and the "residence time" is as defined just above. The surface temperature excursion following transition is shown in Figure 7. It is possible to simulate this boiling transition behavior with a much smaller experimental facility called mini-ULPU, in which a 5-cm in diameter heater is made to plunge and retract from the surface of a water pool, at a controlled frequency (see Figure 8). With the surface now more accessible, we can undertake more detailed measurements, and with the convenience of such a small facility, we can study the various factors (surface properties, especially) more efficiently.

Thus, the prediction problem is basically reduced to predicting the frequency of the lens formation and departure cycle. A first attempt at a dynamic model for this purpose is presented by Angelini et al. (1997a). Therein, it is shown that the problem scales with the group $uR/\rho$, where $u$ is the vapor supply velocity, $R$ is the heater radius of curvature, and $\tau$ is a characteristic time given by $(R/g)^{1/2}$, $g$ being the acceleration of gravity. Moreover, we find that both the departure of an initially stationary lens, as well as the growth and departure cycles in the actual process, occur at a dimensionless time, $t/\tau$, of order unity. That is, $t \sim \tau$, which is $\sim 0.5$ s, in agreement with the $\sim 2$ Hz frequency observed in ULPU (and also in the CYBL experiment, Chu et al., 1997).

The Microlayer Depletion Time

Both in ULPU and mini-ULPU we have found that near BC the surface superheat builds to a sufficient level to activate a dense array of rapidly growing vapor bubbles. These bubbles were found to merge within a time period of $\sim 10$ ms
Fig. 5. Conceptual illustration of the formation, residence, and release cycle. The residence time is defined as $\tau_R = t_5 - t_1$.

Fig. 6. Illustration of boiling crisis as the intersection of the vapor lens "residence" time, and the microlayer "depletion" time. The former is from data obtained in ULPU. The latter is calculated based on a microlayer thickness of 110 $\mu$m.

Fig. 7. Illustration of a temperature excursion following boiling transition. Note the rewets.

Fig. 8. Schematic of the mini-ULPU experiment. The test section is meticulously insulated, and the mechanical device needed special care to damp out unwanted vibrations.
(the ‘nucleate’ boiling time $\tau_n$). This yields a microlayer thickness $(2\sqrt{\rho_f \gamma_n})$ of $\sim 100$ $\mu$m, which is consistent with the times required for its evaporation (Angelini et al., 1997). That this is a good ‘reference’ value of the microlayer thickness, relevant to the BC phenomenon, is supported by the mini-ULPU data, shown in Figure 9. The liquid film thickness on the mini-ULPU heater surface can be deduced from these data by the requirement that it is completely vaporized within one cycle. We thus calculate $\sim 450, 270$, and $160$ $\mu$m at $0.5, 1$, and $2$ Hz respectively. Remarkably, the sensitive upper portion of the data ($\omega > 2$ Hz), indicates that this $160$ $\mu$m thickness is an asymptotic value (i.e., $800$ kW/m$^2$ at $2.3$ Hz yields $157$ $\mu$m). Two important points can be made now.

(a) The externally imposed oscillation in mini-ULPU leaves an excess quantity of water on the surface, but this excess diminishes with increases in frequency (and critical heat flux). Ultimately, at frequencies greater than $\sim 2$ Hz, the nucleation density and bubble growth rates are high enough to produce separation of the liquid film (bubble coalescence) prior to the mechanical withdrawal. In this case the liquid film on the heater is the true ‘microlayer.’

(b) As seen in Figure 6, at BC, the contact frequency in ULPU is $\sim 2$ Hz. Based on the above, this is sufficient to produce a true microlayer. Moreover, the order of magnitude estimate of its thickness, $\sim 100$ $\mu$m, is in good agreement with that found in mini-ULPU.

Clearly, more precise estimates can be made, and prediction accuracy could be enhanced, by accounting for such things as nucleation site density (effect of wetting properties, cavity sizes, etc.), liquid subcooling (a small amount present in ULPU due to gravity head), and heating surface superheat, in affecting bubble growth rates and time scales for coalescence. However, the $\sim 100$ $\mu$m estimate should serve, in the meanwhile, as a good, mechanically supported, reference value for well-wetted surfaces. Typical images of this microlayer in the process of dryout are shown in Figure 10.

The Vapor Lens Residence Time

An initial attempt to predict lens growth and departure was made by Angelini et al. (1997). They used a simple mechanical energy balance and several ad hoc elements such as: simplifying the lens shape (as in Figure 5) so that it can be parametrized by only two quantities, the lens thickness ($b$) and the radius ($R_L$); neglecting condensation, as the lens grows into regions with subcooled water; and assuming as an initial condition a proportionality relationship between the lens growth rates in the radial and downward directions. Here we address the problem by direct numerical simulation, which requires no significant ad hoc assumptions.

We use the commercial code FLOW-3D (Hirt, 1987). Calculations were carried out in cylindrical coordinates to simulate the ULPU geometry. In all cases the lens growth is initiated from a 20 cm in “radius” (length, $R_L$ in Figure 5) 2 mm thick, vapor bubble, and the vapor supply corresponds (through the latent heat and density) to the applicable heat flux. The vapor is taken to behave isothermally. Two kinds of calculations were carried out. In type A runs we keep the vapor supply area constant, equal to the initial value, and we allow no condensation. In type B runs the vapor supply area is modified at each time step to include the newly exposed heater surface to the vapor in the lens. Since this area is very close to the area of the liquid-vapor interface (where condensation is to occur if subcooling exists), we can use the results of series B runs (heat flux as the parameter) without condensation, to predict the behavior for any, separately estimated, condensation rate, by adding the corresponding heat flux to the flux of the heater (this will be made more clear below).
Fig. 10. Visualization of the dryout phenomenon in mini-ULPU. The area shown is a 10 mm x 20 mm (horizontal x vertical) region on the heated surface.
Typical pressure histories are shown in Figure 11. We see an initial, inertia dominated region, of ~50 ms, and a rather rapid transition to continuity-controlled growth for the remainder of the time. In the earlier simple model, an initial velocity had to be assumed in lieu of this early inertia region.

In Figure 12, we show typical results of vapor lens shapes, through the growth-departure cycle. The relatively flat base is in good agreement with the shape assumed in our previous, simple model (Figure 5). However, we see a rather significant difference at the radial front, and at the juncture of this front with the base. Sample results of the lens thickness (at the pole, the "b" in Figure 5) variation with time are shown in Figure 13. Note that growth is more gradual, while the collapse portion of the cycle is rather sudden. This is in agreement with visual records from the experiments, and contrasts to the symmetrical behavior predicted by the simple model.

The most important results, the lens residence times, are summarized in Figure 14. In this figure, the series A runs are shown without accounting for any condensation. The lens dynamics is controlled solely by buoyancy, and the residence time is seen to be rather insensitive to the heat flux (vapor supply). The series B runs, in the same figure, are shown scaled with a condensation heat flux of 200 kW/m². This was estimated from direct contact condensation on a turbulent eddy (Theofanous et al., 1976) having a characteristic, newal, time of 0.015 s, and condensing under constant subcooling of 10 K (as in ULPJ). These results are similar to those of series A, but exhibit a slightly greater sensitivity with heat flux. Finally, as seen in the figure, both results are quite close to the data obtained in the ULPJ facility. Clearly, more precise estimates for any given situation would require knowledge of the liquid subcooling (it may vary as a function of the distance from the heating surface), and of the turbulence length and velocity scales. Since the lens surface area (liquid-vapor interface) remains quite close to that of the heater in contact with it, the above series B results can be scaled back or forth, depending on the condensation rate predicted — provided, of course, that this rate remains approximately constant with time, as used in the calculations. Note, however, that due to the slight sensitivity with heat flux this adjustment for condensation is not very important. For more complex situations with variable rates, the FLOW-3D calculations need to be run again.

Discussion

To gain some perspectives on effects, uncertainties, and implications, we have put together the above results, to construct Figure 15. The microlayer depletion times are shown for thicknesses of 110 μm ± 20%. The lens residence times are shown for a saturated system, as well as for a constant condensation rate of 200 kW/m². We can see that for a saturated system the CHF can be as low as 300 kW/m², as indeed was found in ULPJ under pool boiling, saturated conditions. We also see that with condensation typical of that found in ULPJ under natural circulation, the CHF may be as high as nearly half the pool boiling (upward facing) value. This was found in ULPJ, too.

We can conclude now that the basic mechanisms are at hand, and that we have the first order "a priori" predictions. These in turn suggest the directions for further quantitative refinements and scaling (i.e., the effect of changing the radius of curvature). These results also suggest approaches to test (using mini-ULPJ), and improve CHF performance in engineering applications.

Guided by the mechanisms outlined above, we can speculate that lower head penetrations (narrowly confined to the centermost portion of the lower head) are unlikely to affect lens departure, and consequently the coolability limits. Still, it would be prudent that this be checked by properly scaled experiments. Moreover, there would appear to be much room for improving performance, especially by appropriate thermal insulation designs that maximize the benefit of convection. This work could certainly benefit from guidance by the basic understanding outlined above. Finally, fin-like structures will have to be considered, as relevant mainly to the ex-vessel context of the IVR technology.
Fig. 12(a). Selected FLOW-3D predictions of lens growth and departure histories Run type A at 500 kW/m².
Fig. 12(b). Continued. Run type B at 200 kW/m².
Fig. 13. Predicted variation of lens thickness (at $\theta = 0^\circ$) with time. The A and B curves correspond homologously to run types, carried at 500 and 200 kW/m² respectively.

Fig. 14. The prediction of the lens residence time in comparison to ULPU data. The series A results are without condensation, while series B is scaled for a condensation heat flux of 200 kW/m².

Fig. 15. CHF prediction scheme. The microlayer depletion times are shown for 110 $\mu$m ±20%. The legs residence times are shown for saturated liquid ($\cdots\cdots$), and for a condensation heat flux of 200 kW/m² (———).

3. MELT PROGRESSION CONSIDERATIONS

By melt progression, here we mean all the intermediate evolution between core uncoverage and initial heatup, through the rapid oxidation (of cladding) phase and the formation of a molten pool in the core region, to the initial relocation event of interest for the explosion loads (Figure 2a), and to the fully relocated core state of interest to the thermal loads (Figure 2b). This progression is notoriously complex, and it should be rather clear that, even if a detailed enough mechanistic model could be written down, its validation would constitute an essentially impossible task. The principal reason is that the process involves, locally, multiple melting, motion, refreezing cycles—that is, inherent uncertainty, which is magnified by superposition, and non-feasibility of prototypic enough experiments. Rather, what we need is an approach that is basic-principles-based, and captures the main features of interest here. These, in turn, derive from three basic questions, directly related to the two aspects of our problem:

(a) What is the relative likelihood of the sideways (Figure 2a) versus a downward (through the core support plate) relocation path?

(b) Is the reactor vessel adequately submerged at the time the debris and thermal loads on the lower head become significant?

(c) Can the core melt relocation process create intermediate debris configurations delivering thermal loads greater than those characteristic of the final state (Figure 2b)?
The answers to these questions depend very much on system design, and the evaluation must include applicable scenarios in a rather comprehensive and detailed manner. The AP600 assessment is indicative of the approach and attention to detail required. A brief summary follows.

On the first question, it was possible to conclude that the downwards relocation path is physically unreasonable. This conclusion was based on the heat sink potential at the lower end of the fuel bundle, and on the radiative coolability (to the water and core support plate below) of bottom-end blockages, having all kinds of compositions and configurations. Moreover, this conclusion was bolstered by detailed consideration of the further heat sinks and transient freezing phenomena below the fuel region, including the cladding in the fission gas plenum, the Zr plug (see Figure 16), and a 1.4-cm-thick stainless steel plate, perforated with 0.48 cm holes to an area fraction of \( \sim 35\% \), found across the top of the bottom-end-fitting. This region would present a second strong barrier, should a crust within the fuel pellet region were ever to rupture. The relevant time frame was estimated from the times required to melt the core and penetrate the 13-cm-thick radial reflector (a special feature of the AP600) and core barrel, illustrated in Figures 17 and 18, and it was shown that this is well within the time of heat sink effectiveness (for blockage coolability)—as estimated from the time required to vaporize the water to a level below the core support plate, and the thermal inertia of the 36-cm-thick core support plate itself.

Fig. 16. Illustration of AP600 lower fuel assembly and core support plate features.

Fig. 17. Cross-sectional geometry of the reactor vessel and reflector.

Based on these results, a timeline of the accident could be put together as illustrated in Figure 19. The cavity flooding and consequent vessel reflooding status was obtained from thermal hydraulic calculations carried out by Westinghouse (J. Scobie) using the code MAAP4.0. The "fast" scenario arises when the break that caused the accident is inside one of the two so-called valve rooms, and the exact timing of it depends on the size of the drains in these rooms. For our purposes, this is enveloped by the "medium" scenario which, as well as the "slow" scenario, arises from the water level rising to the top of the reactor vessel, under the operation of two or one lines of the cavity flooding systems, respectively. First, we see that even with half of the cavity flooding system assumed failed, the reactor is completely submerged at the time of the first relocation. This answers the second question above, and allows that the fragility in Figure 4 be applicable. Second, we note that the "medium" scenario leads to vessel reflood close enough with the first relocation event that it may well stabilize most of the core material to within the reflector region. This is the "in-core" IVR context mentioned in the Introduction, and it could be further improved upon by appropriate design features that allow the vessel to passively reflood at times that encompass the uncertainty in the timing of the first relocation. Third, it is clear that, except for a short, essential singularity in time, the relocated debris can only encounter saturated water in the lower plenum. This is important for the evaluation steam explosion loads, as discussed further below.
Finally, one the third question, the key is in the quantities and behavior of the metallic in-vessel structures. The mechanism of concern is called “focusing,” and involves a thin metal layer on top of a large oxidic pool. Simply put, if radiative heat transfer on top is inadequate to discharge the thermal power received from below, the temperature of the metal layer would increase, and increasing amounts of energy flow would be directed to the vessel wall. This “focusing” would increase as the metal layer thickness decreases, until the thermally-induced turbulence in it is sufficiently low for it to sustain significant radial thermal gradients. Assuming the emissivity of a pure metal (0.4), this focusing effect becomes threatening to the vessel wall of the AP600 at a thickness of ~20 cm, and no case has been made so far that this can be mitigated by radial gradients. So, the question translates to whether the quantities of metal involved in a significant quantity of relocated melt can be so low as to limit its thickness to under ~20 cm. For the AP600 with 40 tons of steel each in the radial reflector and the core barrel, and 25 tons in the core support plate, the height of the metallic pool is estimated at over 0.8 m, i.e., well outside the focusing zone. As expected, this aspect of the evaluation attracted major attention in the peer review, and the reader is referred to the exchanges, and Appendix O of DOE/ID-10460 for further elaboration of relevant melt progression mechanisms. Further, it was noted that a focusing-induced failure, were it to occur, would be very localized, and hence inconsequential to the debris retention coolability of the lower head. Moreover, such a failure would allow vessel reflood and an easier still stabilization and coolability of any not-yet-relocated debris.

4. LOADS EVALUATION METHODOLOGY

The AP600 assessment is founded on the Risk Oriented Accident Analysis Methodology (ROAAM) (Theofanous, 1996), the principal aim of which is to handle epistemic uncertainty in a manner that is robust and persuasive. Towards this end, the methodology includes as key elements the following.

(a) A consistent integration of stochastic and deterministic elements is used to identify the appropriate severe accident management “window.” In particular, a screening frequency is employed to discard sequences that are “remote and speculative,” and for those that cannot be so discarded, a physics-based approach is used to make failure in question “physically unreasonable.”
Problem decomposition is employed to explicitly identify and distinguish between well-behaving quantifiable portions and inherently unpredictable ones. The former are called "causal relations" (CRs) and are subject to the normal engineering practice of scaling, prediction, and assessment of uncertainty. The latter are called "intangibles" (Is), and they can only be subjectively assessed. A special probability scale is used for the temporary "quantification" of the intangibles, as probability density functions (pdf's), so as to allow the necessary integration of the various parts to a whole. The manner in which this is to be done is specified by a logical structure called a "probabilistic framework." The "quantification" of each intangible is done in a conservative fashion.

Scenario splits that are of an intangible character are pursued without regard to any "judged" relative likelihood. These are called "splitter scenarios" and are pursued comprehensively, except for those that can be shown to be "physically unreasonable."

A broad array of independent experts, so as to cover well all subject areas, are engaged as critics and reviewers of a fully developed and documented case, not in the usual expert elicitation style for developing the case. The review is pursued until all issues are resolved, and a clear documentation is kept and published at the completion.

The actual implementation of these procedures in treating the two problems discussed here is probably the hardest to summarize within the physical and time confines of this lecture. The reader is referred to the two main documents—DOE/ID-10460 and DOE/ID-10541—and here we limit our attention to the two probabilistic frameworks only. They are shown in Figures 20 and 21, and discussed briefly in the following.

Fig. 20. Schematic of the probabilistic framework utilized in evaluating explosion loads for IVE in AP600.

Fig. 21. Schematic of the probabilistic framework utilized in evaluating thermal loads for IVR in AP600.

Referring to Figure 20, we can see that the framework converts any specified melt relocation rate to an effective wall impulse through the use of two computer codes, the PM-ALPHA.3D and ESPROSE.m-3D. They calculate the premixing and propagation of steam explosions, respectively. The "composition map histories" in the figure refer to the time-sequence of spatial distributions of melt, water, and steam volume fractions. These maps "quantify" premixtures, in the sense that they strongly affect triggerability and the energetics of an explosion propagated through them. The melt relocation rate is specified as an intangible, while the two computer codes are in the role of causal relations. Two other intangibles, the degree of breakup during premixing, and the timing of the explosion trigger, are conservatively
enveloped in the manner described in Section 5. Verification of the “fitness for purpose” of these two codes has also been discussed.

Referring to Figure 21, we can see that the framework converts any specified input decay power, fraction of Zr oxidized, and quantity of metal in the melt, to a heat flux distribution to the wall, through the use of a simple model for the convection–radiation link (CORAL). The three inputs are specified as intangibles, and the model is in the role of a causal relation. A fourth intangible, the metal surface emissivity, is conservatively enveloped, and normal uncertainties in properties are reflected by randomly sampling normal distributions through specified ±2σ ranges. Verification of “fitness for purpose” of the model is discussed in Section 5.

Details on the specification of the intangibles, for both problems, can be found in the original documents.

5. QUANTIFICATION OF THERMAL LOADS AND INTEGRITY ASSESSMENT

The bulk of the decay power is in the oxidic pool, and the principal consideration in addressing a problem such as the one illustrated in Figure 2b concerns the partition of this power into energy flow through the upper (horizontal) and lower (curved) pool boundaries. Oxidic crusts surround the melt on all sides, imposing an isothermal boundary, so that the problem is completely specified by the geometry, power density, and melt properties. The appropriate scaling groups are the internal Rayleigh number (Ra') and the Prandtl number, and the relevant value for them, for the specific case considered here, are \(10^{10}\) and \(0.6\), respectively. For large reactors with high power density, the Rayleigh number may be greater than \(10^{17}\).

Correlations for the Nusselt number take the form

\[
Nu_t = C Ra'^n Pr^m
\]  
(2)

and the values of \(n\) and \(m\) are such as to suggest small length scale and Prandtl number dependencies. The \(Nu_t\) and \(Pr\) numbers are defined in the usual way

\[
Nu_t = \frac{q_i L}{(T_{\text{max}} - T_w)} \quad \text{Pr} = \frac{\nu}{\alpha}
\]  
(3)

where \(q_i\) is the heat flux through boundary \(i\), \(T_{\text{max}}\) and \(T_w\) are the liquid bulk and wall temperatures, respectively, \(L\) is an appropriate length scale (the height or equivalent height of the pool), and \(\alpha\) and \(\nu\) are the thermal diffusivity and kinematic viscosity, respectively. The internal Rayleigh number is actually the number of an external Rayleigh number (Ra) and the Damköhler number defined by

\[
Ra' \equiv \frac{\beta g \dot{Q} L^5}{\alpha \nu k} = \frac{\beta g(T_{\text{max}} - T_w)L^3}{\alpha \nu} \cdot \frac{\dot{Q} L^2}{k(T_{\text{max}} - T_w)} \equiv Ra \cdot Da
\]  
(4)

where \(\dot{Q}\) is the volumetric power density and \(\beta\) and \(k\) are the fluid thermal expansion coefficient and conductivity, respectively. The internal Rayleigh number is used in volumetrically heated (internally-driven) systems, because the power density, rather than the temperature difference, is the independent variable. The converse is true in cavities heated and/or cooled from the outside; that is, they are externally-driven, and the Ra rather than the Ra' is then appropriate.

The Damköhler number will be recognized as equal to the \(Nu_t\) number for cases in which \(\dot{Q}L\) represents the heat flux through boundary \(i\) — this is exactly true in cavities with only one boundary cooled (upper), or heated (lower), and approximately true in horizontal layers with both boundaries cooled, if the lower stratified (conduction) layer is very small compared to the whole depth. In all other cases, the Damköhler number is significantly greater than \(Nu_t\).

The problem of finding an appropriate specific expression for Eq. (4) has been in the requirement that in experiments both the Ra' and Pr numbers must match the prototypic ranges, in a similar geometry, while the answer cannot be found in numerical simulations in that the thermal turbulence and stratification mechanisms have not yet been properly closed. On the other hand, direct solutions of the Navier–Stokes equation at the Ra' numbers of interest are prohibitive, even with today’s computers. It is interesting that, notwithstanding these difficulties, all previous work insisted in seeing the internally-driven problem as something very special and thus failing to take advantage of its relation to a corresponding externally-driven problem as suggested by Eq. (6). For example, Cheung (1980) repeated, for the internal problem for a horizontal layer, the theory of Kraichnan (1962), originally developed for the external problem. Also, due to experimental difficulties, the Prandtl number dependence in Eq. (4) has remained unknown. The relationship began to be exploited only recently with the introduction of the ACOPo experiment (Theofanous and Liu, 1995 and Theofanous et al., 1996c).

The basic idea is that at fully developed turbulence (high enough Rayleigh numbers) the physics of the rate processes in the two problems should be exactly the same. Further, recognizing that for large enough volumes the time constant of boundary layer adjustment to changes in bulk conditions is short, compared to the time constant of the bulk change itself, the sequence of quasi-steady states in an externally-driven transient cooldown could be identified as an equivalent set of true steady-states with internal heating. In the ACOPo experiment, this takes the form of a 2 m in
diameter hemisphere (half the reactor scale), heated to some initial temperature, and then allowed to cool by suddenly imposing a lower temperature at all boundaries. This is done by circulating coolant through a constant temperature bath, and the “jacket” that forms the boundary is segmented (see Figure 22) so as to allow the measurement of the local heat fluxes. Experiments conducted with water at initial temperatures near \( \sim 100 \, ^\circ C \), and the boundaries at \( \sim 0 \, ^\circ C \) reached internal Rayleigh numbers of \( \sim 10^{10} \), and led to the correlations

\[
Nu_{up} = 1.95 \, Ra^{0.18}
\]

\[
Nu_{dn} = 0.3 Ra^{0.22}
\]

\[
\frac{Nu_{dn}(\theta)}{Nu_{dn}} = 0.1 + 1.08 \left( \frac{\theta}{\theta_p} \right) - 4.5 \left( \frac{\theta}{\theta_p} \right)^2 + 8.6 \left( \frac{\theta}{\theta_p} \right)^3 \quad \text{for} \quad 0.1 < \frac{\theta}{\theta_p} < 0.6
\]

\[
\frac{Nu_{dn}(\theta)}{Nu_{dn}} = 0.41 + 0.35 \left( \frac{\theta}{\theta_p} \right) + \left( \frac{\theta}{\theta_p} \right)^2 \quad \text{for} \quad 0.6 < \frac{\theta}{\theta_p} < 1
\]

used in the AP600 assessment. In Eqs. (7) and (8), \( \theta_p \) is the angular position at the top of the oxidic pool, the pole being the 0°.

Fig. 22. Schematic of the ACOPO test vessel, showing the individual cooling units and the vessel support.

A further step in exploiting the similarity noted above was made more recently (Theofanous and Angelini, 1997), with the help of special ACOPO experiments run with only the top boundary cooled. The principal result is shown in Figure 23, which unifies the well-known Steinbner-Reineke (1978) correlation for the internal problem, and the also classic Chu-Goldstein (1973) and Garon-Goldstein (1973) correlations for the external problem. Thus, it unambiguously demonstrates the ACOPO claim made above. The unified result is

\[
Nu_{up} = 0.206 \, Ra^{0.303} \, Pr^{0.084}
\]

where properties for the Ra and Pr numbers are evaluated at the bulk fluid temperature, which is important for the ACOPO data with maximum \( \Delta T \)'s of \( \sim 100 \, ^\circ C \). The thermal conductivity in Nuellt numbers is based on the film temperature, but this is not important. Also important in this unification was to bring in the Prandtl number dependency, which in fact has been observed in some externally-driven experiments but remains neglected theoretically. Here this was derived mainly from ACOPO, with \( 2 < Pr < 7 \), but it agrees with the 0.074 value of Globe and Dropkin (1959).
Fig. 23. Nusselt number dependence on external Rayleigh number. In the plot all previous listed correlations are printed, over the full range, as dotted lines.

Beyond the exact similarity between the two problems demonstrated in Figure 23, the fundamental significance of it is in showing that the theoretically predicted 1/3-law (Kraichnan (1962) is not reached, even at Rayleigh numbers as high as \(10^{13}\). This contradicts the results of Goldstein and Tokuda (1980) that the 1/3-law is reached already at \(10^{10} < Ra < 10^{11}\). In fact, the ACOPO results taken in the succession of previous data shown in Figure 21, indicate a much more gradual but unmistakable transition from the 2/7- to the 1/3-laws, as illustrated in Table 1 below. The other fundamental significance of these results is that no sudden regime transition can be expected as \(Ra \to \infty\), contrary to some recent ideas falling under the name “hard turbulence” (see, for example, Caistang et al., 1989 and Belmonte et al., 1994). In particular, the \(-1/7\) Prandtl number dependence that characteristically arises in these models is shown here to not be true.

The practical implication of this unification is that the thermal boundary layers in all horizontal boundaries, within both the oxidic pool and the metal layer can be treated on the same basis, using Eq. (9), which can be taken as verified up to Ra and Ra' numbers of \(10^{15}\) and \(10^{16}\), respectively. This by far exceeds the matching requirements for the AP600, and due to the very gradual change in exponent shown in Table 1, it could safely be extrapolated to whatever Ra' number range is required in larger reactor applications.

<table>
<thead>
<tr>
<th>Corr.</th>
<th>Ra Range</th>
<th>(m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>C-G</td>
<td>(2.76 \times 10^5 - 1.05 \times 10^8)</td>
<td>0.278</td>
</tr>
<tr>
<td>G-G</td>
<td>(1.36 \times 10^7 - 3.29 \times 10^9)</td>
<td>0.293</td>
</tr>
<tr>
<td>S-R</td>
<td>(2.9 \times 10^{10} - 1.07 \times 10^{11})</td>
<td>0.304</td>
</tr>
<tr>
<td>Eq. (9)</td>
<td>(2 \times 10^{10} - 10^{13})</td>
<td>0.303</td>
</tr>
</tbody>
</table>
Using these results, the up-to-down heat flux split can be readily estimated from

\[ R' = \frac{Nu_{up}}{Nu_{dn}} = 6.5Ra^{-0.64} \tag{10} \]

which for Ra' numbers equal to 10^{16} and 10^{17}, respectively, yields 1.63 and 1.49, respectively. For any given oxidic pool volume and power density, the total power can be thus partitioned, and the heat fluxes across each of the boundaries can be found by dividing-in the respective areas. The (average) flux through the curved boundary can then be used in Eqs. (7) and (8), to determine the local fluxes, and thus assess coolability through the use of Figure 4 or Eq. (1). For the AP600, this shows huge margins, and it can readily be seen that coolability is possible even for much higher power densities. For example, for a debris of volume V that fills the hemisphere completely, using an energy balance and Eq. (10), we obtain

\[ q_{dn} = (1 + 0.5R')^{-1} \frac{QR}{3} \sim 0.2QR \tag{11} \]

Now, scaling the power density directly according to power (in reality it would be lower) from those of the AP600 (~2 GW, \( Q \sim 1.3 \text{ MW/m}^3 \)) for a 3.4 GW reactor, we obtain \( Q = 2.2 \text{ MW/m}^3 \), and the above equation yields an average heat flux of 0.88 MW/m^2. Then, from Eqs. (7) and (8) and Figure 4, we can see that there would be absolutely no problem at the bottom (\( \theta \sim 0^\circ \)). The upper end (\( \theta \sim 90^\circ \)) needs to be looked at closer in terms of the specifics, since the delivered heat flux of \(~1.76 \text{ MW/m}^2\) is higher than \( q_{cr} \). But the difference is small, and the result in Figure 4 does not include any features to optimize performance.

To complete the assessment, the heat flux through the upper boundary, obtained in the manner explained above, can be used to drive the metal layer.

A universal representation of the convection–radiation processes in the metal layer has been written as

\[ T_b' = T_{\ell, o} + T_{\ell, m} \tag{12} \]

\[ q_{up} = T_{\ell, o}^4 + \left( \frac{H_{f}}{R} \right) \left( T_{\ell, o} - T_{\ell, m} \right) \frac{4}{3} \tag{13} \]

\[ q_{\ell, w} = \frac{1}{2} \left( T_{\ell} - T_{\ell, m} \right) \frac{4}{3} \tag{14} \]

where

\[ T_b' = \frac{T_b}{T}, \quad T_{\ell, o} = \frac{T_{\ell, o}}{T}, \quad T_{\ell, m} = \frac{T_{\ell, m}}{T}, \quad q_{up} = \frac{q_{up}}{\bar{q}}, \quad q_{\ell, w} = \frac{q_{\ell, w}}{\bar{q}} \tag{15} \]

with

\[ \bar{T} = \left( \frac{A}{c_{o}} \right)^{3/8}, \quad \bar{q} = \epsilon \sigma \bar{T}^4, \quad A = 0.15k \left( \frac{g_{f}}{\alpha_{f}} \right)^{1/3} \tag{16} \]

and solved once and for all. Results in graphical form, convenient for hand calculations, can be found in Theofanous et al. (1996a). A sample is provided in Figure 24, and the focusing problem can be visualized with the help of Figures 25 and 26. In the above equations, \( \sigma \) is the Stefan–Boltzman constant, \( \epsilon \) is the emissivity, \( T_b, T_{\ell, o}, \) and \( T_{\ell, m} \) are the bulk, layer top surface, and the vessel wall liquidus (in contact with the metal layer) temperatures, respectively, \( H_f \) is the metal layer thickness, \( R \) is the radius of the vessel, and \( q_{\ell, w} \) is the heat flux to the vessel wall in contact with the metal layer. This latter is the quantity of interest, to be determined and compared to the thermal failure criteria, at the appropriate angular position.

A sample of results of a detailed analysis using a model that includes all other details (which turn out to be mostly unimportant to the conclusions), and the complete framework discussed in Section 5 is shown in Figure 27. The result of an extreme parametric case carried out to test the failure boundaries due to the focusing effect is shown in Figure 28.

Regarding future applications to larger, higher power density reactors, the rough estimates of fluxes provided above indicate that the focusing effect can become a real problem. Special attention in this regard needs to be paid to internal design features, as they affect melt progression and relocation phenomena, and the quantities of molten metal involved in them. It also may be worth looking out for any radial transport limitations in large aspect ratio metal layers, as noted above. Finally, in case all these fail to produce adequate, robust results, IVR should be considered in the "ex-vessel" context noted in the Introduction.
Fig. 24. Sample solutions to the system of Eqs. (12) – (14). Each group of $H_e/R$ lines corresponds to one value of $T_{e,m}$. The four values that correspond to the four groups shown are 0.116, 0.126, 0.134, and 0.141—these are in the order of increasing values for $q_{up}$ for the starting points of the four solid lines ($H_e/R = 0.1$) that lead each of the four groups.

Fig. 25. Solution to the systems of Eqs. (12) – (14) in physical variables, with the emissivity as the parameter. $q_{up} = 400 \text{ kW/m}^2$, and $A = 2764 \text{ W/m}^2 \cdot \text{K}^{4/3}$. For the chosen value of $q_{up}$, the peak heat flux at the edge of the oxidic pool is 350 kW/m$^2$, and the intersections shown represent the values of $H_e/R$ below which the heat flux at the layer wall contact would exceed this value. These signify the “focusing” threshold.

6. CONCLUDING REMARKS

The in-vessel retention idea, as a severe accident management strategy, is well-based on both, fundamental and practical considerations. It is of proven validity for low power density, medium size reactors, such as Loviisa and the AP600 design. For very large, high power density reactors, the concept requires a closer examination, and perhaps some help from appropriate, new, design features. More importantly, the IVR technology could be beneficially employed to arrest an accident either inside the core barrel, or ex-vessel, in a core-catcher that resembles the lower head. It is important to recognize that the ex-vessel application allows considerably more flexibility than is possible in-vessel, and thus opportunities to optimize and accept much larger, and higher power density cores.

Regarding the thermal loading aspects of the problem, the technology is sufficiently mature, but it would be prudent to also accumulate the real-materials experience as provided by the international RASPLAV program at the Khurtatsav Institute.

Regarding the explosion loading aspects, IVR provided the impetus for important fundamental advances in the past few years, so that, now, a rational approach to this problem begins to appear possible. This work is likely to have a long lasting impact to areas much wider than IVR, or even nuclear reactor safety.
Fig. 26. Solution to the system of Eqs. (14) - (16) in physical variables, with the imposed upward heat flux as the parameter. $\epsilon = 0.45$, $A = 2764 \text{ W/m}^2$, $k^{4/3}$. The threshold values of $H_e/R$, obtained as in Figure 23, are shown by the 's.

Fig. 27. Probability distributions of the heat fluxes normalized by the local $q_{ef}$ for selected positions around the lower head. The position $\theta = 85^\circ$ corresponds to the top of the metallic layer. The double-hump behavior at position $\theta = 75^\circ$, arises because of “sharing” the oxidic pool and metallic layer contact.

Fig. 28. Results of an extreme parametric study carried out to “test the failure boundaries.” $H = 1.18 \text{ m}$, $H_e = 0.22 \text{ m}$, and $\dot{Q} = 1.4 \text{ MW/m}^3$.

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REFERENCES


GAREC analyses in Support of In-Vessel Retention Concept

Participants to GAREC Working Group:

The GAREC group has performed an extensive analysis of In-Vessel Retention capability for actual and future French PWR designs. This analysis includes: scenario analyses for core meltdown and corium transfer to the lower head, corium behaviour in the lower head (debris and corium pools), external cooling and optimisation of external water flow path, mechanical behaviour of the vessel, risk of vapour explosion and dynamic behaviour of the eroded vessel.

This analysis is based on the most recent experimental results available from internal R&D, on the conclusions of the Vessel Investigation Project concerning TMI 2, on the conclusion of the work performed in support of AP 600 [Theofanous et al., 1996], on advanced calculation tools (MAAP, TOLBIAC, CATHARE, CASTEM) and on engineering evaluations.

1. Reactor characteristics considered

A geometry typical for French reactors (900 MWe and 1450 MWe) is considered (fig 1). The lower head has a hemispherical shape (radius 2m and 2.5m respectively, thickness 15 cm) which is attached to a cylindrical body by means of a junction piece which presents a short (about 20 cm large) downward oriented horizontal surface. The RPV is thermally insulated with a cylindrical honey comb structure. The distance between the outer surface of the vessel and the inner surface of the thermal insulation is small (about 1 cm) over the cylindrical section of the vessel. No penetrations crossing the lower head are considered. For future reactor designs, the horizontal part of the junction piece might be eliminated and the position of the external thermal insulation might be changed. The pressure in the primary circuit during late phases of severe accidents is considered not to exceed 20 bars through appropriate use of the primary circuit depressurization systems. The influence of the implementation of heavy reflectors for future reactors is also considered.

2. Physical situations analysed and bounding situation

Core meltdown starts from core uncovering and includes core oxidation, melting, relocations, formation of corium pools, slumping of corium in the lower head. This scenario may be interrupted and at least delayed by late water injections. The main uncertainty in Physical situation analyses proceeds clearly from the instant and rate of late water injections in the
RPV. In the worst considered situation no late water injection occurs and only the presence of a residual water volume in the lower head is considered before core relocation («dry» situation). Reflooding of the remainder of the core at different times of the core degradation and relocation have been considered («wet» situations). These situations include safety injection recovery. Physical situation analyses without late water injection indicated that the time needed for complete core melt-down is at least 2 to 3 hours after shutdown from full power conditions. By late water injection, the progression of the accident might not stop immediately, as was the case in TMI2.

«Wet» and «dry» situations are analysed, however, the «bounding» case considered with external passive cooling, from the point of view of long term thermal loads on the lower head is the case where the complete inventory of core materials (with variable masses of structure metals) is relocated as a corium pool in the lower head with external passive cooling.

3. Relocation processes
The relocation process into the lower head is supposed to be initiated by a corium flow through the lateral baffles whether or not the water is present in the RPV. This is justified by the fact that, as soon as a corium pool is formed in the core, the heat flux at the lateral upper boundary of the pool greatly increases and induces a radial propagation of the molten materials. The downward relocation within the core region is limited by the fact that oxidic materials relocate and freeze rapidly on previously and independently moving metal crusts as it appears from the interpretation of MP2 experiment [Gasser et al., 1996]. When the molten material touches the vertical baffle plate, this plate melts (in the presence of water, the heat flux exceeds the Critical Heat Flux which may be removed by the flow within the actual baffle arrangement). The precise position and size of the breach cannot be predicted. The position is within the upper lateral maximum heat flux region of the molten pool. Thus the mass of corium which is delivered to the lower head is limited to the mass of liquid which lies above the breach level (in TMI2 this corresponds to about 30 tons). The corium flow rate is limited by the size of the breach and also by the size of the holes in the baffle plates over which the corium spreads. During this first flow, the melt is delivered to the lower head in several jets over a large perimeter, which is favourable for the obtention of premixing conditions. The estimated maximum instantaneous delivery rate in the lower head in TMI2 is about [Anderson et al., 1989] 1,5 ton per second. The melt needs a few seconds to travel from the bottom plate to the bottom of the lower head. Thus it may be estimated that the melt which is instantaneously mixed with the water contained in the lower head may be of the order of a few tons. Part of this melt may flow in kinds of «lava tubes», as has been hypothesised for the formation of the hard layer in TMI2, and is thus insulated from the water by a crust [Wolf et al., 1993].

For dry situations, the volume of residual water in the lower head will decrease both due to the freezing and cool-down and residual power dissipation in the relocated corium (about 1 m³ water for 1 ton corium). A relocation of about 20 to 40 tons is expected to evaporate the 20 to 40 m³ residual water inventory in the lower head. Thus, for later corium flow in dry situation, it is expected that almost no water will be available in the lower head.

After the first flow, the space in the baffle may be more or less filled with solidified material. A second flow may reasonably occur after several minutes or even tens of minutes. This flow may again pass through the still open space in the baffles or may flow through a breach in the core barrel into the downcomer. In that case the corium jet may impact the cylindrical wall of the RPV before flowing to the lower head. In absence of water ("dry" situation), the RPV may also be heated by radiation from the liquid corium surface through the breach. This may threaten the integrity of the RPV wall if the RPV is not cooled from the outside at this level. The failure time has been estimated in the range 15 to 30 minutes after breach formation.

For late relocations, a breach in the bottom part of the core may also occur.
Scenario analyses for a core with a heavy reflector indicate that, due to the increase of the delay necessary for the melt-through of the heavy reflector (about an additional half an hour) the possibility of a first release at the core bottom cannot be excluded for a "dry" situation. **In this case a large oxidic corium jet (but short duration) in the residual water cannot be excluded**. For wet physical situations (core region covered with water), the possibility of early relocation at the bottom of the core is not considered possible.

4. Jet impacts

Physical situations with heavy reflector can include the existence of a molten steel layer (10 to 25 centimetres thick) laying onto the oxidic pool in the core (the steel comes from the ablation of the heavy reflector). This metallic layer is not expected to be much superheated (max 100°C above melting temperature). In this case, and for a dry situation, if the first break-through appears in the heavy reflector, a steel jet may impact the RPV. **This steel jet (even if not very superheated) may threaten the integrity of the RPV, even if the RPV is cooled from the outside**. The estimated melt-through delay is in the 1 min to 5 min range. During this time interval, the location of the jet impact will move (due to the decrease of the height (jet driving pressure) of the emptying metallic layer) and this effect is generally calculated to be able to save the integrity of the RPV for reasonable assumptions on the diameter of the jet (diameter increase due to the melting of the wall of the heavy reflector by the metallic jet is taken into account).

**Other situations** involve mainly oxidic jets. Melt-through by an oxidic jet requires 1) impact of a continuous liquid jet at the same location, 2) sufficient duration (a minimum of 2 minutes continuous impact) is required for an oxidic jet with 200°C superheat impacting at 7 m/s (the heat transfer correlation due to [Saito et al., 1990] has been used together with the assumption of an existing (maybe unstable) crust) 3) absence of « pool effect » (this effect, which is due to the gathering of liquid in the ablation hole, reduces the ablation kinetics).

Physical situation analyses show that satisfying all these conditions together is very difficult. For a given volume of corium released into the lower head (say, typically 2,5 m³) a long duration (2 minutes) requires a small jet flow rate (20 kg/s); considering an impact velocity of 7m/s (due to pure gravity fall over 2,5 meters), this corresponds to a jet diameter at impact of 2 centimetres. Under water, such a jet will breakup within a short distance (breakup correlation is used due to [Saito et al., 1988]) and will not be able to threaten the integrity of the RPV wall. When water is removed, the lower head will be protected by the presence of the previously relocated debris. Jets with large diameters (case of first break-through at core bottom) are likely not to breakup during their travel through the residual water layer; but their duration is then much shorter than the 2 minutes required for RPV melt-through. Furthermore it is likely that long duration jets will not be able to impact at the same location; this is due to the fact that the flow rate of the jet will not be constant (thus impact place will change if the jet is not strictly vertical) and also to the fact that jets may be unstable due to edge and attachment effects when they leave the structure upon which they flow. However these latter effects have not been taken into account in the actual state of the physical situation analyses because they are difficult to quantify.

5. Debris formation and behaviour in the lower head

5-1. Dry situation, absence of external cooling

Due to the presence of residual water, the first 20 or 40 tons of melt released from the core will relocate as debris or hard layers in the lower head. From the time of complete evaporation of water, it will take a few hours to re-melt the quenched debris. During this time interval, the heat load on the lower head will be mainly due to the hard (compact) layers which are in direct contact with the wall and, progressively, to the molten corium coming from the core which gathers above and within the debris. The heat loads due to molten corium pool formation, accumulating on top of the debris, exceeds the heat transferred by the underlying solid debris. From this point several physical situations have been examined for
different debris configurations, including metallic materials. The general conclusion of these studies is, for instance for a 900 MWe French PWR, that the vessel will fail within 30 min to 60 minutes from the time of the first corium relocation to the lower head if for instance the core melts completely within 3 hours. A heat flux concentration due to the presence of a metallic layer is not predicted to reduce significantly the failure delay; this is due to the fact that the potential failure by focusing effect is only « efficient » if there is enough available power delivered to the metallic layer to produce a significant focused heat flux. This is only possible if a quite large amount of oxides is gathered in the lower head, which requires some time.

These evaluations indicate that, at the time of vessel failure, a variable mass of liquid corium may be present in the lower head (30 to 80 tons) together with still solid but hot material (25 tons). The vessel may fail before the complete mass inventory from the core has been transferred to the lower head. The failure mode (location and breach size) is still an open question. The failure is expected to occur at low pressure (between 1 and 20 bars overpressures), elevated vessel temperatures (about $1200^\circ$C => creep failure) with strong 3D temperature distribution on vessel surface, and small temperature gradients (100 to 300°C) over the vessel thickness.

5-2. Wet situations, absence of external cooling

If water is re-introduced in the RPV the preceding time delays will be increased. The question to what extent a corium pool may be cooled within the degraded core has no clear answer. A stabilisation of the corium pool within the core is, in essence, a problem of knowledge of critical heat flux on the surface of the « crucible ». As the crust is surrounded by debris whose size distribution and porosity are not known, the problem cannot be easily solved. In TMI2 the maximum heat flux in the end state crucible configuration is estimated to be 0.25 MW/m². This heat flux could be removed at about 100 bars. This is an indication that the critical heat flux was, under TMI2 conditions, higher than 0.25 MW/m². The question now is: how can this be extrapolated to much lower pressures?

The question of the gap formation and debris coolability in the lower head is also open. The hard layer could be cooled in TMI2 and a scenario based on gap opening explaining this coolability has been proposed by [Henry and Dube, 1994]. However, in TMI2 only a small amount of corium (20 tons) is relocated in subcooled water at elevated pressure. If water is injected only after the onset of RPV melting in contact with the relocated material, or if relocated materials contain molten metals which are binding to the vessel wall, the gap opening mechanism is questionable. Also the size effect, related to the relocation of larger amounts (say 30 to 60 tons) of corium debris in the lower head is not known: a compact layer may cover previously relocated debris and decrease the amount of water capable of reaching the lowest regions of debris in contact with the wall. Furthermore, the correlation by [Monde et al., 1982] which is used for the evaluation of the rewetting heat flux cannot be extrapolated to low pressures and very large distance/gap ratios. Representative experiments are required.

The thermal behaviour of debris beds in water is well known as long as the bed is homogeneous. But the debris bed in the lower head is probably heterogeneous with local solid accumulations, liquid corium layers and even empty zones which induce heat flux peaks and flow instabilities which affect strongly the heat transfer. The experimental facility SILFIDE in EdF is dedicated to the investigation of heterogeneous debris beds coolability.

5-3. Wet situations with external cooling

The question of gap formation is no longer a problem if external cooling is considered. In this case small heat fluxes delivered by the molten layers to the vessel wall, excluding a large molten pool which will be treated as the « bounding » case, can be removed by external natural convection flow without threatening the vessel wall.
5-4. Dry situation with external cooling; the bounding case

If no water is available in the RPV, remelting and corium pool formation cannot be avoided. Even if water is present, partial remelting cannot be excluded. The bounding situation is a situation in which the whole corium content of the core is transferred to the lower head with a variable amount of structure material. An oxidic corium pool is expected to form in the lower part with an overlying metallic layer of variable thickness. As an example, the oxidic pool occupies the whole hemisphere volume in the case of a French 900 MWe reactor. The thickness of the metallic layer may vary from 10 cm to 50 cm. 10 cm may be the minimum thickness when only the metal ablated from the internal surface of the vessel at steady state is considered.

5-4-1. Power dissipated in the corium

The power dissipated in the corium pool is generally estimated by supposing that the volatile fission products (I, Cs, ...) are removed during the early stage of the accident. Refractory and less refractory species (like Ba and Sr) are supposed to stay in the melt. With these assumptions, the residual power variation for a 900 MW reactor is presented in table 1:

<table>
<thead>
<tr>
<th>Time after shutdown</th>
<th>1h</th>
<th>2h</th>
<th>3h</th>
<th>10h</th>
<th>20h</th>
<th>10 days</th>
<th>20 days</th>
<th>70 days</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power (MW)</td>
<td>34</td>
<td>26</td>
<td>24</td>
<td>17</td>
<td>14</td>
<td>7</td>
<td>4</td>
<td>2</td>
</tr>
</tbody>
</table>

Table 1: Residual power variation as a function of time for a 900 MWe French reactor (end of cycle).

Recent VERCORS experiments [André et al., 1996] have shown that a significant amount of Ba (70%) was released from the fuel at 2300°C. Lanthanum is a decay product from Baryum and is responsible for about 40% of the residual power 1 hour after shutdown. Thus the feedback of the release of Baryum may significantly decrease the residual power. Evaluations indicate that the residual power may be decreased by, about, 20%. Further verification and research would be necessary.

About 10% of the residual power is expected to be dissipated in the metallic layer.

5-4-2. Molten material behaviour

The dissolution of zirconium during core melt-down was expected to result in the formation of a UOZr mixture whose solidification range lies between 2000°C and 2600°C. Nevertheless, recent RASPLAV experiments show a stratification of the melt into two layers: a heavy, mainly oxidic, layer and a light, mainly metallic, layer [Asmolov et al., 1998]. The separation is attributed to density effects rather than to physico-chemical effect (miscibility gap) [Gueneau et al., 1998]. This stratification, the reason for which is not well understood at this time and must be further investigated, tends to simplify the problem of the corium behaviour. Each layer would have its own liquidus temperature which is considered as the boundary temperature condition for thermal-hydraulic calculations. Furthermore the metallic layer could merge with the molten steel and form a thick metallic layer which is expected to decrease the focusing effect (see § 5.4.5).

As the metal layer contains zirconium and uranium, it may dissolve the steel of the vessel wall. Some calculations of this effect have been performed [Froment et al., 1998]. These calculations show that the interface temperature between molten metal layer and steel wall is about 1100°C. Thus the dissolution cannot affect the cold part of the externally cooled vessel which ensures the mechanical retention capability.

The viscosity of the molten corium layer, above liquidus is known and is small [Merziakov, 1998], [Sudreau and Cognet, 1996].

5-4-3. Heat Flux distribution from the oxidic pool

If a metal layer is considered to lie above the oxidic pool a crust will form in contact with this layer and also in contact with the vessel wall. Thus a uniform temperature will be imposed as boundary condition for the oxidic corium pool. This temperature is considered to be the Liquidus temperature corresponding to the composition of the oxidic melt [Seiler, 1996]. The steady state heat flux distribution then depends only on the geometry of the pool. The most
recent experimental results, performed for Rayleigh numbers which are characteristic for reactor conditions (ACOPO [Theofanous and Angelini, 1997], BALI [Bonnet et al., 1998] and COPO II [Kymäläinen et al., 1998]) indicate that:

- for a hemispheric pool, the power directed to the upper surface of the pool is equal to about 40% of the total power
- the heat flux distribution on the lower hemispheric part shows a peaking factor (max flux/average flux) of 1.75; this heat flux is quite uniform over the upper third of the pool height
- the heat flux at the bottom of the pool is very small (conduction limited)

For a maximum power of 30 MW (900 MWe reactor, 1 hour after shutdown [remark: this is a maximum power since complete core meltdown is not expected to occur within less than 3 hours after shutdown]) and a vessel radius of 2 meters, the mean heat flux on the wall is 0.7 MW/m² and the maximum heat flux is 1.25 MW/m². For 20 MW (3 hours after shutdown), the mean heat flux on the wall is 0.45 MW/m² and the maximum heat flux is 0.8 MW/m².

Heat transfer correlations have been established which permit the evaluation of the superheat of the melt within the pool. This superheat is about 200 °C for a hemispheric pool, 2m in radius, with a power dissipation of 1MW/m³.

5-4-4. Residual wall thickness

The vessel wall will be ablated due to the heat flux coming from the pool. The external temperature is imposed, approximately, at saturation temperature. The thermal response time of the vessel wall is about half an hour. The residual thickness of the vessel is given, in steady state, by a simple conduction calculation and the temperature gradient is quite linear with melting temperature at the inner surface. A residual thickness of 4.5 cm is calculated for a heat flux of 1 MW/m² and 3 cm at 1.5 MW/m².

The temperatures in the residual wall will decrease with the decrease of the residual power. Table 2 presents the evolution of the mean temperature in the residual wall as a function of time:

<table>
<thead>
<tr>
<th>Time after shutdown</th>
<th>1 hour</th>
<th>2 hours</th>
<th>4 hours</th>
<th>1 day</th>
<th>10 days</th>
<th>70 days</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mean residual wall temperature (°C)</td>
<td>800</td>
<td>630</td>
<td>550</td>
<td>390</td>
<td>240</td>
<td>140</td>
</tr>
</tbody>
</table>

Table 2: Evolution of mean temperature of residual wall thickness as a function of time.

5-4-5. Heat flux distribution from the metal layer

5-4-5-1. Focusing effect, minimum thickness of metal layer

The heat flux transmitted through the upper surface of the oxidic pool is re-distributed to the upper and lateral boundaries of the metal layer. If no water is present (dry situation), the heat transmitted through the upper free surface by radiation alone will be limited and the main part of the power may be re-distributed on the sides of the metal layer. Thus the lateral heat flux is, approximately, proportional to the inverse of the metal layer height and, for small heights, may exceed the maximum heat flux delivered by the oxidic layer to the wall. If this heat flux has to be limited, for example, to the equivalent of the maximum heat flux delivered by the oxidic pool to the wall, the thickness of the metal layer must exceed a minimum thickness which depends on the assumptions concerning the heat transfer at the free surface of the metal layer: for a radiative surface, with a global emissivity equal to unity and surface temperature of 300°C on structures in the vicinity, the minimum thickness must exceed 1/8 times the radius (i.e. about 25 cm, about 25 tons steel).

The melting of the inner surface of the lower head provides about 10 tons of molten steel (10 cm layer thickness) which is not enough to avoid a focusing effect. At least 20 or 30 additional centimetres of molten metal should be available.

The recent RASPLAY AW200-1 and 2 are, from this point of view, very interesting since these tests show a possibility that a metal rich layer may separate from the oxidic pool. 20 centimetres represent, roughly, 15 tons zirconium which corresponds to the Zr inventory of a
900 MW(e) reactor core. However the mechanism of this separation is not well understood. There is interest for a better understanding of the mechanism and of the effects that additional species may have (iron, nickel, chromium).

Furthermore, some amount of metal will be available from the melting of internal structures (structures in the lower head, support plate, heavy reflector, ...) which will contribute to increase the thickness of the metal layer.

This problem is still open and further attention should be focused on these aspects in the future, including transient aspects during the build-up of the oxidic pool and metallic layer (shallow metal layers are expected to be most dangerous).

If water is added on top of the metal layer, the problem of the focusing effect disappears.

5.4.5.2. Reduction of focusing effect due to a radial temperature gradient in the metal layer

For shallow metal layers (h/L<0.1, h is the height of the layer and L is the width) the validity of that approach is questionable which consists in the calculation of an upwards average heat flux (\(q_{up}\)) and a sidewards average heat flux (\(q_{side}\)) from an average metal layer temperature (0D approach). In fact, for shallow layers, a radial temperature gradient is expected to appear in the metal layer due to flow partitioning in the layer (maximum temperature on the axis of the layer and minimum on the side). To investigate this effect a specific test has been performed with water in the BALI facility in a 2D slice geometry. In this facility the heat delivered by the oxidic pool to the metal layer was simulated with a bottom heating plate, the fluid was cooled laterally at constant temperature (presence of an ice layer) and a resistance simulated radiation heat transfer at the surface of the layer. For further details, the reader should refer to: [Bonnet et al., 1998]. The conclusions of these tests are that:

- a radial temperature gradient appears within the layer for h/L<0.1,
- this temperature gradient induces a reduction of the average lateral heat flux. This reduction increases when the thickness of the metal layer decreases. Nevertheless the reduction remains small. For h/L=0.025 the average lateral heat flux is reduced by about 20% when compared to the 0D calculation approach,
- effects of eventual non-uniform heat flux distribution delivered by the oxidic melt to the metal layer could be investigated in the experiments including a reduction of the heat flux delivery at the edges of the layer. For constant power delivery and constant layer height, non-uniform heat flux distribution does not induce significant variation of the average lateral heat flux,
- lateral heat flux distributions could also be measured; the measurements show a heat flux concentration near the top of the layer.

The interpretation of these tests is still going on. A physical model for the calculation of the radial temperature gradient will be proposed. 2D slice results will be extrapolated to reactor geometry (2D axisymmetric).

5.4.6. Water injection on the pool, thermal-hydraulic aspects

If water is finally introduced in the RPV on a molten pool with a metal layer, only the metal layer and the vessel at this level will be partially cooled but the oxidic pool will remain unaffected (since the boundary temperature is still imposed to the liquidus temperature of the oxidic melt); thus the quenching energy to remove will be small. With some assumptions on the heat transfer mode between molten steel and water (mainly: the heat transfer surface is supposed to be equal to the flat surface of the metal layer), transient calculations indicate that the pressure will stay below 20 bars provided that the relief valve has an inner diameter greater than 4 cm. However, local interactions between the water falling on the molten metal layer may result in a greater efficiency of the heat transfer and thus a more rapid vapour production. Experiments are necessary to investigate this effect (CEA : ANAIS project).
The rewetting in the reactor situation differs from the rewetting of a simple molten steel layer by the fact that an important heat flux (due to the upward heat transfer in the oxidic pool) must be evacuated to the water (about 0.8 MW/m² at maximum residual power (30MW, 1 hour after shutdown) in a 900 MW reactor, 0.5 MW/m² in reactors with larger vessel dimensions and for the same power level). The problem, for such elevated heat fluxes, is that solidification on the free surface is not straightforward. We have assumed that solidification occurs only below 1400°C (which corresponds to the freezing temperature of steel or of oxidic compounds containing mainly iron oxides). According to calculations made using the Berenson correlation ([Gad Hestroni, 1982] heat transfer in film boiling augmented by radiation heat transfer), it is concluded that solidification occurs for heat fluxes which are smaller than:

- 0.4 MW/m² if an emissivity of 0.42 (value measured by [Theofanous et al., 1996] for liquid steel) is considered
- 0.7 MW/m² if an emissivity of 0.8 (corresponding to oxidic material) is considered.

Thus it is concluded that, on the basis of existing correlations, freezing on the free surface cannot be guaranteed at elevated (short term) residual power levels. This may have some consequences on the risk of vapour explosion at the surface of the metal layer.

However, it should be considered that the meltdown of the core, the formation, in the lower head, of a corium pool and of a metal layer will take at least a few hours and thus the residual power dissipation will be significantly reduced. For instance 10 hours after shutdown, the heat flux crossing the metal layer will be of the order of 0.4 MW/m² for a French 900 MWe reactor. This aspect must be taken into account in future evaluations.

5.4.7. Effects of crust instabilities

Effects of crust instabilities (crust at the interface between oxidic pool and vessel, crust at the interface of the oxidic pool and metallic layer) have been investigated. In fact oxidic crusts are very thin and may break from time to time. For the analysis, a periodic destruction of the crust has been assumed. The minimum value of this period has been taken to be equal to the time of rebuilding of the crust. With these hypotheses, this phenomenon affects the residual thickness of the vessel; but only a fraction of the inner steel layer (which is at elevated temperature) is further ablated without significant effect on the thickness of the external ligament at low temperature which takes over the mechanical loads.

5.4.8. External Cooling

Most recent experiments ULPU [Theofanous et al., 1996], SULTAN [Rougé, 1995] and SBLB [Cheung et al., 1997] show that, for a hemispherical external surface and no obstacles to the water flow, coolability of the lower head under natural convection may be obtained for heat fluxes on the upper hemispherical part as elevated as 1.6 MW/m². This heat flux level seems to be an upper bound: the establishment of a strong natural recirculation flow requires gross boiling (positive quality) in the upper part of the hot leg and the results obtained on SULTAN indicate that CHF occurs for negative outlet qualities for heat fluxes higher than about 1.5 MW/m². Higher heat fluxes could not easily be removed since the mechanical integrity will be difficult to guarantee for an internal pressure of 20 bars. Nevertheless this conclusion is not applicable to the geometry of actual French reactors since:

- the assembly piece between the lower head and the cylindrical part of the RPV presents (figure 1) a horizontal, downward oriented, surface which may favour vapour accumulation, and, thus, reduce CHF;
- the distance between the vessel and the thermal insulation is rather small.

SULTAN experiments have been performed in such a way that the results can be used for the optimisation of the flow path for future reactors.

5.4.9. Mechanical behaviour of the vessel
Analyses of the mechanical behaviour of the vessel have been performed. The conclusion is that the externally cooled 16MND5 steel could withstand the mechanical loads for a heat flux of 1.6 MW/m² provided that the internal pressure stays below 20 bars.

The thermal shock due to reflooding of the RPV is not expected to threaten the integrity of the vessel. The main part of the metal is expected to cool down progressively: cooling is slowed down by the heat conduction delay in the wall. Only a layer of less than one centimetre thickness will cool down faster than 20 K/second which is capable of changing the crystalline structure of the metal. This phenomenon affects mainly the inner hot part of the metal.

However, transient changes from compression to tensile loads [Bhandari et al., 1998] may induce local damage whose effects on the wall behaviour is not understood.

Complementary investigations are clearly necessary.

Another point of concern is the potential decrease of mechanical characteristics of the steel by diffusion under tensile loads of some elements like Tin, Cadmium, Indium or Zirconium from the metallic corium into the wall. A literature survey performed by [F. Barbier and I. Cingolani, 1997] shows that the true fracture strength of AISI 4140 steel may for instance be reduced by 20% above the melting point of Cadmium or Tin. However, in the real reactor case, these elements should have to diffuse through the temperature gradient from the hot part of the wall to the cold ligament; the possibility of such a migration has to be established. Therefore a specific experiment has been realised in ISABEL [CEA-DCC Saclay] (results reported in [Froment et al., 1998]). In this experiment the vessel is simulated by a small crucible externally cooled containing a metallic corium mixture including Tin. The heat flux delivered to the vessel is about 1MW/m². The test wall is molten from the inside and representative temperature and stress gradients are established through the wall thickness. At the end of the experiment, the analysis shows that the average composition of the corium mixture in contact with the 16MND5 steel is (wt%): 65.5 Fe-8.7 Cr-7.1 Ni-14 Zr-2.7 Sn. In such conditions and in the vicinity of steel, the solidified corium exhibits a biphasic structure composed of (Fe,Cr) and (Fe,Zr)-rich phases. At the corium/steel interface, a concentration profile of chromium, nickel and manganese is observed for about 50 μm in the steel. However, no profile of tin and zirconium is detected. Therefore, taking into account the accuracy of the chemical analyses, it seems that the volume diffusion of tin in steel is not significant, but no information is available on the possible presence of tin along the steel grain boundaries. In the present experiment, this transition zone remains very small and it seems unlikely that the mechanical properties of the steel be drastically affected beyond this zone. Nevertheless, only mechanical tests performed with micro-specimens could help to solve entirely the problem.

6. Vapour explosion

Vapour explosion may, potentially, occur in, mainly, two situations:
1. during a jet flow from the core to the lower head,
2. during reflooding of molten corium pool.

6.1. Mechanical loads which lead to vessel failure

6.1.1. Intact uneroded vessel

Order of magnitudes of mechanical loads which lead to vessel failure have been evaluated for a pressure pulse acting simultaneously on the whole surface of the lower hemispherical head; the pressure variation is supposed to follow: \( P = P_{\text{max}} e^{-t/\tau} \).

When the characteristic duration of the pressure pulse (\( \tau \)) is smaller then the period corresponding to the fundamental mode of vibration of the structure (\( \tau_{\text{struct}} \)) (impulse mode) the impulse (\( I = P_{\text{max}} \tau \)) which leads to failure is given by:
\[ i_{pl}^2 = 2 \varepsilon_r \sigma_y e^2 \rho \]  

(Equation 1)

In the case where the characteristic duration of the pressure pulse is larger than the period corresponding to the fundamental mode of vibration of the structure \((\tau > \tau_{struc})\), the equivalent impulse which may lead to failure is approached by:

\[ i_{eq} = \left( i_{pl} \right) \left( \frac{1}{1 - \frac{P_{elast}}{P_{max}}} \right) \]  

(Equation 2)

Where:
- \(i_{pl}\) is the impulse necessary for plastic rupture of the structure
- \(\varepsilon_r\) is the real elongation at rupture
- \(\sigma_y\) is the yield stress corresponding to the structure material
- \(e\) is the thickness of the vessel wall
- \(\rho\) is the density of the steel
- \(P_{elast}\) is the static pressure corresponding to the maximum elastic deformation of the vessel
- \(P_{max}\) is the peak pressure relative to the vapour explosion.

The effects of other forms for the pressure variation due to vapour explosion have also been investigated [Lepareux, 1996] such as: \(P = P_{max} \left(1 - \frac{t}{\tau}\right)e^{-\frac{t}{\tau}}\). For order of magnitude purposes we will limit here the discussion to the results obtained with: \(P = P_{max}e^{-\frac{t}{\tau}}\).

The period corresponding to the fundamental mode of vibration of the cold lower head \((\tau_{struc})\) is equal to about 2 ms. For a elongation to rupture \((\varepsilon_r)\) set at 20%, a yield stress set at 600 MPa (cold vessel), a wall thickness of 15 cm, equation (1) leads to an impulse of 2,2 \(10^5\) Pa.s. This means that the rupture of the vessel may be obtained in the impulse mode for pressure peaks of the order of 1000 bars for \(\tau = 2\) ms, 2000 bars for \(\tau = 1\) ms or even more for shorter pressure peaks. If the duration of the pressure peak is significantly greater than 2 ms, the maximum allowable pressure to which the vessel could withstand tends to the static pressure corresponding to the maximum elastic deformation \((P_{elast})\) which is of the order of 500 bars for the cold lower head.

6.1.2. Eroded vessel

For the eroded vessel the wall is weakened but the mass which has to be accelerated (vessel + corium) is significantly increased. The period corresponding to the fundamental mode of axial vibration of the weakened lower head filled with corium \((\tau_{struc})\) is increased (about 4 to 6 ms). Mechanical analyses tend to show that the peak pressure should however not exceed the maximum pressure compatible with elastic deformation of the weakened vessel, which lies in the 40 to 80 bars range for residual thickness of a few centimeters.

6.2. Corium masses participating in the interaction

6.2.1. Corium jets to the lower head

In case of external cooling, the lower head is quite cold and intact when corium jets penetrate into the lower head (not much corium accumulated => low heat fluxes to the vessel, water available in the lower head). Corium flowing through the former plates or through the core barrel is not expected to present large mass flow rates (see § 3). Furthermore oxidic corium freezes quickly (crust formation kinetics of the order of 1 mm/s in the first seconds). Thus, only a « limited » amount of corium will be able to mix with the
water in the lower head, the estimated upper limit is about 2 tons. Order of magnitude calculations indicate that 2 tons are unlikely to produce necessary mechanical loads to break an intact uneroded vessel.

The mechanical loads will decrease significantly if only residual water is available in the lower head (absence of hydrodynamic confinement, water at saturation temperature). If water fills the RPV, the increase of the hydrodynamic confinement is calculated to lead to higher mechanical loads. However, the internal structures will be cold in that case and should be able to absorb a significant part of the mechanical energy of a slug travelling to the vessel lid.

The worst situation is that of a large corium jet released from the bottom of the core. This physical situation could not be eliminated in the case of the presence of a heavy reflector which needs additional time to melt through (see § 3). However this physical situation might be restricted to the case where only residual water is available in the lower head since it seems difficult to melt the crust in the lower part of the corium pool in the core (minimum heat flux) when water covers this pool. In such a case, a significant mass of corium may be mixed but only with a reduced mass of water near to saturation temperature. The consequences of such a situation should be further investigated.

It is clear that the risk of vessel break by FCIs linked to corium jets into the lower head may be strongly reduced and even eliminated by scenario analyses. Nevertheless, these scenario analyses are, at this time, clearly not sufficiently quantified and need complementary efforts.

6.2.2. Reflooding of corium pools with water

It is true that the reflooding of the corium pool contained in the lower head with a low and controlled mass flow rate cannot lead to an energetic FCI provided that:

1. The water injection mass flow rate is low and well controlled
2. A crust forms at the interface between the melt (probably a metallic layer) and the water.

The formation of a crust is not straightforward for large reactors (1400, 1800 MWe). This is due to the fact that the estimated heat fluxes which have to be evacuated to the water layer are elevated (in the range 0.5 to 1 MW/m²), as discussed in (§ 5.4.6), and the melting temperature of steel or oxidic compounds with iron oxides are « low » (about 1400°C). With elevated heat fluxes, water may stay separated from the melt by a vapour film and accumulate above the molten pool. If local FCI events are not triggered spontaneously as and when water is fed, a large water accumulation may lead to a global FCI which then threatens the integrity of the eroded vessel.

This problem should be further investigated. Some recent data are available from JAERI [Park et al., 1997] on small experiments; large scale experiments are necessary. An experiment (ANAIS) is planned in France.

7. Possible design improvements for In-Vessel Retention

The design of future reactors may be improved on the following points, if In-Vessel Retention is considered:

Design improvement for In-Vessel retention by external cooling:

a) optimisation of the form of the lower head; suppression of the horizontal part in the junction piece
b) optimisation of the external flow path. A double wall could be designed in order to reduce the amount of water (reduction of external flooding delay, suppression of risk of FCI in the reactor pit if vessel would nevertheless fail) and in order to increase the recirculating water flow rate by natural convection. This would increase the margins to CHF. The results obtained on SULTAN are pertinent for such an optimisation. This double wall should be mechanically resistant to water hammer effects due to vapour condensation. Indeed, important water hammer effects have been observed in SULTAN; these effects increase when the subcooling of the water increases.
c) implementation of a sufficient mass of steel in the lower head in order to avoid the focusing effect (may be melting of heavy reflector is sufficient).

Specific design for In-Vessel retention:
Eventual implementation of internal « crucibles » in the lower head, creating a pre-existing gap, which could promote in-vessel coolability of debris [Szabo and Richard, 1997], [Suh and Hwang, 1997]

8. Conclusions, further R&D needs
The analysis of the bounding case shows that the vessel is expected to withstand the thermal and mechanical loads provided that:
- the heat flux stays below 1.5 MW/m² in the vertical lateral part of the vessel,
- the pressure in the RPV will stay below 20 bars,
- the external flow path has been optimised in order to get enough margins to CHF.

The main open questions concern:
- the heat flux concentration factor due to a metallic layer (focusing effect) both during the formation of the corium pool and quasi steady state (BALI metal layer tests). From this point of view the separation of UOZr mixture into heavy oxide and light metal layers, observed in RASPLAV, is of interest and should be further investigated and better understood. Complementary material tests are thus necessary.
- the risk of vessel melt-through by corium jets with special emphasis on jets crossing the core barrel and impacting the cylindrical part of the vessel (some work on corium jets is going-on at FzK)
- the rewetting of the metal layer, crossed by a significant heat flux, by water injection in the RPV; the formation of a solid crust at the surface of the metal layer (ANAIS project)
- the risk and consequences of vapour explosions in following situations:
a) water layer above a molten metal layer in absence of crust formation,
b) corium release in form of a strong jet from the bottom of the core into residual water pool in the lower head.

These latter questions are addressed by the development of MC3D (code describing jet fragmentation, premixing and interaction) and supporting validation experiments (IPSN Programme) in France and also at FzK and JRC Ispra.

Other open questions with lower priority should also be considered:
- re-investigation of the species and amounts of fission products released before melting. If, for instance as is evidenced by VERCORS experiments, it may be admitted that Ba is released, the residual power level might decrease significantly (~20% to 30%) which reduces significantly the heat loads on the vessel.
- analyses of the possibility of fragilisation of steel by diffusion of Sn, In (ISABEL Test)
- further investigation of mechanical behaviour of the vessel at reflooding of the RPV (CEA-DMT, Framatome and FzK)
- limit of coolability of corium pools in the core and of debris and dense layers in the lower head (In France some work is going-on on debris beds coolability at EdF).

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1. Introduction

The report presents an executive summary of the accomplishments along the main lines of activity in the first phase of RASPLAV Project.

RASPLAV Project was implemented by the Russian Research Centre "Kurchatov Institute" in cooperation with a number of scientific and research & production associations of Russia: IBRAE RAN, NPO Luch, NPO Elektrotherm, NPO TermiIKS.

RASPLAV Project of the Organisation of Economic Cooperation and Development (OECD) was initiated in 1994 as a three-year program of research in the behaviour of a pool of reactor core prototypic materials melt in a reactor lower head. In 1989, long before the initiation of the Project, Kurchatov Institute proposed to start a joint research of this problem. The bilateral research agreement between the US Nuclear Regulatory Commission (US NRC) and Kurchatov Institute was the first step towards implementation of this Project. The interactions of corium with different materials were investigated within the scope of a series of small-scale experiments. The data, which had been obtained at that stage, gave the first experience in handling high-temperature core materials under laboratory conditions. Calculation and theoretical investigations of this problem were begun in parallel.

These investigations served as a basis for working out proposals for the Committee on the Safety of Nuclear Installations (CSNI) to organise this Project which was submitted for consideration of the member-states in 1992.

Intensive discussions with experts allowed to state clearly the principal objectives, technical approaches and anticipated results of the Project.

The agreement on the OECD Project of investigations into the interaction of fuel melt with reactor vessel bottom was prepared and signed by the OECD member-states and Russia in July 1994. This OECD Project, which was the first project to be implemented in a non-member country, united the efforts of 14 OECD member-states and Russia. (Later in 1996 Czechia and Hungary joined the Project).

The Russian Research Centre "Kurchatov Institute" was appointed the operating agent of the Project and it implements now the research plans in compliance with MB decisions and recommendations of the technical experts of PRG.

The principal objectives of the first phase of RASPLAV Project were as follows:

• Development of a technology of high-temperature experiments with prototypical materials of reactor core melt which will allow heating and maintaining a major part of 200 kg loading at a temperature above the liquidus temperature (over 2600 °C);

• Conduct of confirmatory large-scale experiments to investigate the behaviour of the melt of core prototypical materials reactor vessel lower head as well as to determine principal differences in the behaviour of the melt as compared with simulating liquids;

• Conduct of supporting small-scale experiments to determine basic the behaviour of the melt at high temperatures as well as
main thermophysical properties required for experiment analyses;
- Conduct of salt experiments to study melt heat transfer processes and to justify the choice of the procedure of large-scale experiments;
- Development of an analytical model and computer program for analysis and interpretation of the results of confirmatory and supplementary experiments and pre-and post-test analyses.

2. Project Experimental Section Feasibility Study

Prior to the RASPLAV Project there were practically no data on the properties of reactor core material mixtures containing uranium and zirconium dioxides and metallic zirconium. The measurement technique was limited to temperatures around 2300 °C. There were no structural materials available for making an integral facility, whose materials would be able to hold a high-temperature and chemically aggressive melt inside the experimental section under prototypical and controlled conditions. Lack of a calculation tool did not allow to make an adequate analysis of facility design, pre-test calculations, to say nothing about calculations of a real accident in a commercial reactor. Therefore, the main attention in the Project was focused on the investigation of prototypical melts containing basic reactor core components.

Feasibility of a large-scale experiment, including the development of an experimental technique, diagnostic systems, structural materials and ways of their protection against dangerous interactions was studied throughout the first year of the Project. In addition, software tools for analysis and interpretation of experiments were developed.

Preparatory to conducting the first large experiment, important technical problems had to be solved in a series of laboratory and small-scale experiments. The most important ones are listed below:
- Determination of corium prototypical composition for each of the proposed experiments which depends on the geometry of an experiment and heating technique as well;
- Development of techniques for heating reactor core materials up to temperatures above the liquidus temperature (T > 2400 °C);
- Development of a set of compatible materials capable of enduring extreme experimental conditions;
- Solution of the problem of corium melt retention inside the volume of experimental section;
- Development of a measurement technique in a range of temperatures exceeding 2500 °C;
- Development of analytical techniques for experimental result predictions and for analysing these results after the experiment.

Table 1 summarises the scope of experimental efforts in the first phase of the Project.

One of the most important problems, which had to be solved prior to a large-scale experiment, was the problem of geometry and heating technique. Two options were studied:
1. Conduct of experiments in a hemispherical geometry with direct electric heating of the melt; and
2. Conduct of experiments in a plane (the so called slice geometry) by the methods of direct electrical heating and side wall heating (see Figure 1).

Comparative analytical and calculation investigations into the process of natural convection...
caused by volumetric (DEH) and side wall heating (SWH) were carried out to validate experimental approaches. It has been shown that dimensionless numbers, which characterise heat transfer, are sufficiently close. Direct experimental investigations at the RASPLAV-A-Salt facility justified their closeness and demonstrated a possibility to use SWH technique in a large-scale experiment.

Figure 1 Experimental Section Geometry

<table>
<thead>
<tr>
<th>Experiment (series)</th>
<th>Objectives</th>
<th>Corium</th>
<th>Corium mass (kg)</th>
<th>Maximum temperature (°C)</th>
<th>Number of experiments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Laboratory experiments (Tigel, Korpus, Terek, Tulpan)</td>
<td>Properties of materials and their interactions</td>
<td>C-22</td>
<td>up to 5</td>
<td>3000</td>
<td>&gt; 100</td>
</tr>
<tr>
<td>RASPLAV-A-liq</td>
<td>Possibility of melt corium pouring</td>
<td>C-22</td>
<td>12</td>
<td>2900</td>
<td>13</td>
</tr>
<tr>
<td>RASPLAV-AW-2,5</td>
<td>Possibility to use SWH</td>
<td>C-22</td>
<td>12</td>
<td>2450</td>
<td>1</td>
</tr>
<tr>
<td>RASPLAV-AD-2,5</td>
<td>Possibility to use DEH</td>
<td>C-100</td>
<td>40</td>
<td>&gt; 2600</td>
<td>1</td>
</tr>
</tbody>
</table>

When both techniques are used in an experiment, each has its own advantages and disadvantages. Hemispherical geometry is the most prototypical, however, only the direct electrical heating technique can be used in this case. The calculation investigations have shown that in this case the main difficulties are related to a considerable nonuniformity of heat release. In addition, resulting Lorentz electromagnetic forces can distort the pattern conditioned by natural melt circulation. The second approach, which uses slice geometry, requires an appropriate system of compatible structural materials. Two experiments were made to test the approaches. The first experiment - RASPLAV-AW-2.5 - was successfully conducted at a scale of 1:2.5 to a large-scale experiment in February 1995. This test demonstrated a possibility to use slice geometry and SWH technique. A possibility to use the technique of direct electrical heating of corium was the subject of the second experiment - RASPLAV-AD-2.5 (August 1995). It has been shown that the possibility to use DEH technique is limited by a high electric conductivity of corium melts. These two experiments alongside with a big number of supporting calculations allowed to make a choice in favour of SWH experiment in plane geometry. From the analysis of
RASPLAV-AW-2.5 experimental results it is also evident that a successful large-scale experiment requires a significant superheating of the melt above the temperature of liquids (~300 °C).

The necessity to achieve extreme parameters called for development of unique structural and protective materials. A large number of conducted laboratory experiments have finally led to the required combination of protective materials comprising a tungsten protector, tantalum subprotector and side graphite plates which are heated by the induction method. The choice of these materials, whose compatibility had been investigated in the laboratory experiments, was also confirmed at TULPAN facility which allowed large-scale material testing experiments. As a result, all this has shown that the choice of the above materials maintains the integrity of the experimental facility up to the temperatures of 2750 °C. The diagnostic system was tested simultaneously in the same temperature range.

The choice of corium composition was discussed in detail. The corium found in the lower head of reactor vessel after the TMI accident, was primarily composed of oxides with small quantities of metallic components (C-100 corium). However, C-22 corium containing a significant quantity of metallic zirconium was chosen for the first experiment. Such a choice was made, on one hand, due to the fact that this composition has a lower temperature of liquidus and, on the other hand, it has more appropriate properties, primarily, a higher heat conductivity as compared with other investigated compositions. Its significant difference from C-100 oxide corium resided in the presence of a large temperature interval between the temperatures of solidus and liquidus. Thus, this difference constitutes 500 °C for C-22 corium, while for C-100 corium it is only about 50 °C.

It was assumed that this difference could produce a sufficiently high effect on hydrodynamic behaviour of the melt since the presence of solid phase in the so-called mushy zone could result in a higher viscosity due to a higher internal friction. In the course of theoretical and experimental investigations it was discovered that in a wide range of temperatures between solidus and liquidus C-22 corium existed in the form of solid phase with small inclusions of liquid phase whose fraction was about 20% in terms of volume. At the same time, it follows from the theory that a liquid with solid phase inclusions becomes mobile only when the fraction of liquid phase exceeds 40 - 50%. The quantity of solid phase in C-22 corium, which was calculated from the phase diagram and is presented in Figure 2, remains almost constant right up to the temperature of 2300-2330 °C and only at higher temperatures it starts to drop fast. As a practical matter, it means that an effective temperature range, in which the existence of solid phase affects the hydrodynamic problems, is much narrower and is only 50-75 °C, i.e. this range is very close to the range of C-100 corium and in this context there is no significant difference in their thermohydraulic behaviour.
3. Measurements of Corium Main Physical Properties up to Temperatures of 2800 °C

Prior to the RASPLAV Project experiments there were practically no data on the properties of reactor core material mixtures containing uranium and zirconium oxides and metallic zirconium. The measurement technique was limited to temperatures about 2300 °C. Therefore, measurement of material properties in a corresponding temperature range both for liquid and solid phase was one of the objectives of the Project. Basic physical properties of coria of different composition were measured in its first phase including melting temperature, electric conductivity, kinetic viscosity, and thermal conductivity.

The results allowed expanding significantly the material properties database. These data were used for designing the experimental facilities. They can be also used in NPP safety analyses. The scope of performed measurements of corresponding conditions is summarised in Table 2.

<table>
<thead>
<tr>
<th>Type of corium</th>
<th>Composition (wt%)</th>
<th>Measured properties and temperature range</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>UO₂</td>
<td>ZrO₂</td>
</tr>
<tr>
<td>C-22</td>
<td>81.5</td>
<td>5.0</td>
</tr>
<tr>
<td>C-50</td>
<td>80.0</td>
<td>11.5</td>
</tr>
<tr>
<td>C-100</td>
<td>77.8</td>
<td>22.2</td>
</tr>
</tbody>
</table>

Table 2: Matrix of Measurements of Corium Properties
4. Large-Scale Experiments of RASPLAV-AW-200 Series

When the main technical problems were solved and when a large number of calculations, which had been made to validate the structure including analysis of possible uncertainties, showed that one might expect acceptable results of a large-scale experiment, a decision was made to conduct such an experiment. Two experiments were carried out within the scope of the first phase of RASPLAV Project.

A picture of RASPLAV-AW-200 facility is given in Figure 3.

In both experiments the experimental section represented a plane cylindrical structure, 0.4 m in radius and 0.116 m wide, which accommodated about 200 kg of corium. The maximum power, which was fed from the thyristor converter into the induction heating system, was around 300 kW at 1800 Hz and the estimated energy efficiency coefficient - about 40 %. To make the boundary condition close to adiabatic, a special insulating layer was placed on the upper cover. This layer consisted of 41 kg of C-100 corium in the form of briquettes and powder.

A general view of the loading placed into the experimental section is presented in Figure 4.

Figure 3 General View of RASPLAV-AW-200 Facility

Figure 4 General View of the Loading of RASPLAV-AW-200-1 Experimental Section

A protector was used to shield graphite plates. This protector had two layers formed by tungsten and tantalum plates 1 mm and 2.5 mm thick, respectively. The protective properties of such protector were confirmed in a series of small-scale experiments up to temperatures of 2750 °C.

Isothermal boundary conditions at the external boundary of the reactor vessel model were ensured by a water cooling system.

The diagnostic system had about 150 different measurement channels (positions of thermocouples and pyrometers are shown in Figure 5), namely:

- pyrometers to measure corium temperature and heated graphite plates;
- standard high-temperature tungsten-rhenium thermocouples;
- ultrasonic thermometer;
- specially developed high-temperature gas-filled thermocouples;
- standard chromel-aluminum thermocouples for test-wall temperature measurements;
thermocouples for cooling system measurements. When the facility was assembled, a series of test startups ("dry" runs without corium loading) was conducted to adjust and test main engineering systems: thyristor converter, magnetic cores, gas-vacuum system, cooling system. In addition, energy efficiency of the induction heating system and the heat release distribution over the entire area of graphite plates were determined in these experiments. Numerous calculations with the use of dry run results allowed a better understanding of the major factors affecting the facility operation.

Figure 5 Positions of Main Temperature Sensors

The first large-scale of power input is given in Figure experiment was carried out on the 6. 9th of October, 1996. The history

Figure 6 Redistribution of Heat Fluxes During the Transition from Conductive to Convective Heat Transfer

Temperatures, which assured a beyond the temperature of significant overheating of corium liquidus, were achieved in the
experiment. The maximum measured temperature of corium in the course of the experiment was almost 2700 °C (Figure 6). A quasi-steady temperature in the corium melt was maintained for more than two hours.

Figure 7 presents a view of the corium ingot after the disassembly of the induction heating system, protector, and subprotector. A rough contour of the melted part and large corium grains, which are aligned with heat fluxes, are seen on the side surface. A cavity between the melt surface and "bridge" of unmelted briquettes is seen in the upper part of the ingot. The cavity was formed after cooling due to a difference in the densities of liquid and solid corium.

The existence of natural convection in the corium melt was confirmed by various indications. An average heat flux through the test wall in the first experiment was around 100 kW/m². This value was determined relying on the results of temperature measurements in the test wall and was confirmed by water cooling system temperature measurements. A gradual reconstruction of the profile of local flux through the test wall was observed starting with 25,000 seconds (see Figure 6). If at the initial stage the flux profile had a maximum in the lower point of the test wall which is fully governed by the distribution of the heat source in the graphite wall, then at the final stage of the experiment the profile has a particularly pronounced maximum corresponding to the angle of ± 45°. Such a reconstruction of the profile unambiguously points to a convective nature of heat transfer. In addition, the corium thermocouples recorded a transition from convective to conductive heat transfer at the cooling stage.

However, the most interesting and important results have been found in the course of post-test examination of ingots. Figure 8 presents a schematic of RASPLAV-AW-200-1 experimental section ingot cutting, while Figure 9 - corium ingot microstructure in cross-section #9. Two layers with a distinct interface were found in the ingot: the upper layer - a light layer which is enriched in Zr. The fraction of corium melt was ~ 70 %.

U/Zr ratio in the lower heavy layer enriched by U (see Figure 10).

Stratification of carbon, which was contained in the initial corium (~ 0.4 wt%), proceeds simultaneously with stratification of U and Zr. The lower layer is depleted in carbon to 0.05 - 0.1 wt%, while the upper layer is enriched up to 1.5 - 3 wt%.

It should be noted that stratification was not observed in tungsten tube of small diameter (~ 14 mm) which was submerged in corium.

The temperature of full spreading of corium samples, which have been cut from the melted part of the ingot and unmelted briquettes located near the solid-liquid boundary, is 2570 - 2580 °C which is 170 - 180 °C higher than expected prior to the experiment.

The GEMINI code calculations point to the agreement of the observed temperatures and those obtained in the experiment for heavy phase. As regards the light phase, the spreading temperature turned out to be much higher - full corium melting and spreading were not observed even on heating up to 2700 °C. One of the reasons is a sufficiently high content of carbon in the light layer which determines higher values of spreading temperature.
A metallographic analysis shows (Figure 11 and Figure 12) that C-22 corium consists of two phases: light and grey. The grey phase in the "main" corium (lower layer) occupies ~ 80 % of the volume and has an oxide composition \((U_{0.82}Zr_{0.2})O_{1.8}\). In the upper layer this phase occupies ~ 20 % of the volume. The light phase has a metal-like composition \((Zr_{0.96}U_{0.04})O_{0.7}\) and enriches the upper layer up to 80 %.
Figure 13 characterises the distribution of temperature over the tantalum subprotector in cross-section #14.

A melting zone is observed on the ends of the melt (cross-section #1 and #15); temperature of ~2200°C on the subprotector corresponds to this zone which is lower than the temperature of corium melting. It is obvious in this case that corium was heated through convective heat-and-mass transfer from the central part of the melt and a convex form of the ingot ends is an evidence thereof.

RASPLAV-AW-200-2 experimental section has the following specific design features which make it different from the previous one:
- changed geometrical form of the induction heater;
- enlarged cross-section of the magnetic core;
- changed thermal insulation between the induction heater and graphite wall-heater;
- changed design of the clamping device between the test wall and graphite wall-heater;
- changed design of freezing seals.

These changes were introduced into the design to:
- ensure a more uniform temperature field on the graphite wall-heater and tungsten protector;
- decrease temperature in the locations of freezing seals.

The diagram of the installation of measurement system sensors in the experimental section is presented in Figure 14.

The basic sensors are as follows:
- pyrometers for corium temperature measurements (Pyr4 - Pyr6);
- thermocouples for the measuring temperature deferece the test wall and on its inner surface (TW and TWS respectively);
- thermocouples for measuring temperatures of freezing seals (FS);
- pyrometers for measuring temperatures of graphite walls-heaters (Pyr1 - Pyr2) and a number of other sensors for measuring the temperatures of magnetic cores, cooling system, etc.

Figure 14 Diagram of Locations of Measurement System Sensors in Rasplav-AW-200-2 Experimental Section

The technology of briquette the previous experiment (see loading into the experimental Figure 15). section was similar to that in
RASPLAV-AW-200-2 experiment was conducted on the 24th of May, 1997. Figure 17 gives the power input history. In contrast to the first experiment, the heating stage was considerably reduced in the second experiment and the temperature growth rate was approximately two times high than in the first experiment. The maximum recorded flux through the test wall is also almost twice as high as in the first experiment (Figure 18). Figure 16 presents a general view of the ingot (left side) produced in RASPLAV-AW-200-2 experiment after the disassembly of the facility.

A major part of corium (> 70 %) was melted in the experiment. The cavity between the melt surface and upper crust was bigger than in the first experiment due to a leak of corium from the volume of the experimental section.

When the experiment was completed and the facility was disassembled, post-experiment examinations of the ingots were carried out.

Figure 16 General View of the Ingot (Left Side) in RASPLAV-AW-200-2 Experiment

Figure 19 presents a schematic of ingot cutting. Figure 20 shows macrometallographic specimens in some cross-sections of the ingot.
The macrometallographic layers which have a rather distinct boundary. The upper stratification of corium into two layers is of lighter colour, is
porous and is enriched (up to ~40 at%) in ZrO$_2$-based light phase with carbon impurities, an average density of the layer is ~9 g/cm$^3$.

The lower layer is enriched in UO$_2$-based phase (up to ~80%), with an average density of the layer is ~9.9 g/cm$^3$.

Figure 21 presents a variation of $U/Zr$ atomic ratio along the height of the ingot in the central cross-section. The figure shows that the $U/Zr$ ratio drastically decreases in the upper layer.

When the samples cut from the ingot were heated, their spreading temperatures were similar to those at RASPLAV-AW-200-1 facility.

Figure 22 presents the distribution of temperature along the left subprotector (central cross-section). $T_{\text{max}}$ is about 100°C lower than in RASPLAV-AW-200-1 experiment. The same figure shows the diagram of locations of diffusion benchmarks for determining temperatures in the volume of corium.

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**Figure 20** View of Macrozones in Ingot Cross-Sections in RASPLAV-AW-200-2 Experiment.

**Figure 21** Distribution of $U/Zr$ along the Ingot Height in RASPLAV-AW-200-2 Experiment.
The basic results of the series of RASPLAV-AW-200 experiments are compared in Table 3. Figure 23 and Figure 24 present certain results of the comparison of the calculated values and RASPLAV-AW-200 experimental data. The comparison, which was made using CONV 3D program specially developed for analysing the results of the experiments within the scope of RASPLAV Project, points to a good agreement of the results.

Well-developed calculation methods were used to analyse possible uncertainties of the experimental results which were obtained in the corium experiments within the scope of RASPLAV Project.

<table>
<thead>
<tr>
<th></th>
<th>RASPLAV - AW - 200 - 1</th>
<th>RASPLAV - AW - 200 - 2</th>
</tr>
</thead>
<tbody>
<tr>
<td>Corium</td>
<td>C - 22</td>
<td>C - 22</td>
</tr>
<tr>
<td>Corium loading</td>
<td>~ 200 kg</td>
<td>~ 200 kg</td>
</tr>
<tr>
<td>Heat transfer mode</td>
<td>conduction + convection</td>
<td>conduction + convection</td>
</tr>
<tr>
<td>Peak heat flux to the test-wall</td>
<td>~ 130 kW/m²</td>
<td>~ 250 kW/m²</td>
</tr>
<tr>
<td>Molten volume</td>
<td>~ 70 %</td>
<td>&gt; 70 %</td>
</tr>
<tr>
<td>Peculiarities</td>
<td>• Almost complete axial stratification into liquids with different densities and U/Zr ratio</td>
<td>&quot;Transition&quot; stratification • Corium-vessel steel interaction due to molten corium attack</td>
</tr>
</tbody>
</table>

"Living" spreading temperature and physical properties of corium melt during melt process. Redistribution of heat fluxes to the tests wall along the angle in consequence of transfer conduction to convection mode.
5. Main Results of TULPAN Facility Experimental Program

When TULPAN facility was commissioned, a number of experiments were conducted at this facility under startup-and-adjustment conditions. These experiments confirmed the facility behaved as designed.

The sensors of high-temperature measurement system (in particular - pyrometers), which were to be used in RASPLAV-AW-200 experiments, were tested in the next stage.

In addition, a series of experiments were conducted at the facility under a coordinated research program to study "stratification" of corium (experiment T3) as well as crust formation and behaviour on the cooled surface of reactor vessel simulator (experiment T4).

Experiment T3 was carried out using C-22 corium, while experiment T4 used C-100 corium. The choice of corium for the first experiment was governed by a necessity to investigate additionally the process of C-22 corium stratification. The principal objective of the experiment was to investigate stratification in the absence of distinctly pronounced convection in the melt. The choice of C-100 corium for the second experiment was based on the simpler phase diagram of this corium as compared with C-22 corium.

First of all, the results of T3 experiment turned out to be similar to those of RASPLAV-AW-200 experiments. As in the large-scale experiments, stratification of the melt into two layers - light and heavy - was also found after the experiment. The experiment once again confirmed the height variation of this ratio within each layer.

The result of post-experiment analysis of the ingot, which was produced in experiment T4, turned out to be unexpected. A variation of U/2r ratio along the ingot height was also detected like in experiment T3.

Figure 25 presents a general view of C-22 corium ingot after T3 experiment and Figure 26 shows the distribution of U/2r ratio along the ingot height.

Figure 27 gives a view of the ingot and U/2r distribution along its height.
6. Main Results of Investigations at RASPLAV-Salt Facility

The pattern of flow in a ceramic melt in the lower head of reactor vessel is featured by natural convection which, in its turn, is governed by a dimensionless Rayleigh number characterising the relationship between the forces of buoyancy and viscous friction. Characteristic Rayleigh (Ra) numbers are of the order of $\sim 10^{11}$ for RASPLAV-AW-200 experiments with ceramic melt, while for a real reactor accident expected Rayleigh number values can exceed $10^{15}$. It is planned to conduct experiments with more prototypical Rayleigh numbers to investigate heat transfer mechanisms in an intermediate range of Rayleigh numbers.

Special investigations into natural convection of the liquid up to the values of Rayleigh numbers (Ra = $10^{14}$) were carried
out at RASPLAV-Salt facility; one of the objectives of these investigations was to support the efforts to design a corium facility. In particular, the properties of the salts used in the experiments allow simulating such phenomena as formation of crusts and mushy zones as well as their impact on heat- and mass-transfer processes.

Investigations of natural convection were conducted by the method of side wall heating (SWH) in the same geometry which was used at RASPLAV-AW-200 facility. The results of experimental investigations have completely confirmed the conclusion on the similarity of the processes of heat transfer have a heated side wall to a cooled wall if the obtained results be directly compared with the results of heat-generating liquid experiments. Investigations into heat transfer processes with isothermal boundary conditions and natural formation of crusts as a result of external cooling were carried out for the first time. This series was completed prior to the large-scale experiment.

Table 4 presents the main series of experiments which were conducted in the first phase of RASPLAV Project with their conditions.

<table>
<thead>
<tr>
<th>Series N</th>
<th>Salt composition</th>
<th>Purpose/heating technique</th>
<th>Ra</th>
<th>Pr</th>
</tr>
</thead>
<tbody>
<tr>
<td>S0</td>
<td>NaF - NaBF₄ 8:92</td>
<td>Scoping Test SWH</td>
<td>7,1·10¹²</td>
<td>5,2</td>
</tr>
<tr>
<td>S1</td>
<td>NaF - NaBF₄ 8:92</td>
<td>Investigation of heat transfer processes at variation of Rayleigh number/SWH</td>
<td>(0,6-5,0)·10¹³</td>
<td>3,5 - 7,5</td>
</tr>
<tr>
<td>S2</td>
<td>NaF - NaBF₄ 8:92</td>
<td>Investigation of heat transfer processes at variation of Rayleigh number with and without crust</td>
<td>(0,2-2,2)·10¹¹</td>
<td>4,2 - 7,6</td>
</tr>
<tr>
<td>S3</td>
<td>NaF - NaBF₄ 8:92</td>
<td>Investigation of heat transfer processes at variation of Rayleigh number with or without crust/DEH</td>
<td>10¹²-1,5·10¹³</td>
<td>4,4 - 7,5</td>
</tr>
<tr>
<td>S4</td>
<td>NaF - NaBF₄ 25:75</td>
<td>Investigation of heat transfer processes for the region of temperatures between solidus and liquidus</td>
<td>~10¹³</td>
<td>~ 6,0</td>
</tr>
<tr>
<td>S5</td>
<td>Li - NaF - KF 46,5:11,5:42</td>
<td>Investigation of heat transfer processes at prototypical Prandtl numbers (this series will be carried out in the second phase of RASPLAV Project)</td>
<td>10¹⁰-10¹²</td>
<td>0,8 - 4,0</td>
</tr>
</tbody>
</table>
The performed series of experiments enabled:
- direct experimental comparison of heat transfer at volumetric heating (DEH) and SWH;
- investigation of the impact of the crust formation process on heat- and mass- transfer for SWH and DEH as well;
- investigation of melt pool behaviour in a range of temperatures between solidus and liquidus consisting of a mixture of liquid and solid phases.

The investigation results show that in case of volumetric heating in the presence and absence of a crust heat transfer process is identical, within the limits of experimental errors, both in terms of integral mean parameters, such as dependence of a mean Nusselt number on Rayleigh number, and local distribution of fluxes. The comparison of integral heat transfer to the cooled test-wall for SWH vs. DEH for regimes without crust revealed small differences within the limits of 10%. For DEH method integral and local heat transfer was found identical both with and without crust. In case of SWH crust produced insignificant effect on integral heat transfer within limits of 10 - 15% due to some redistribution of local Nusselt numbers. The comparison of the results of salt experiments with the results obtained for a hemispherical geometry points to their agreement.

7. Calculation - Theoretical Investigations

Prior to RASPLAV Project the application of computer programs was limited by 2D calculations of natural convection processes in the liquid. The performed calculations allowed to model heat transfer processes for a free convection of heat-generating liquid with the use of additional models, e.g. model of turbulence for big Rayleigh numbers. At the same time, the requirements for an adequate modelling of the processes at RASPLAV facilities raised a question of development and application of 3D codes. There also arose a necessity for development of a number of corium description models. This task was successfully solved in the first phase of experiments when software tools were developed for analyses and interpretation of experiments. These tools include 2D and 3D codes for simulating convection and heat transfer problems in complex systems. In addition, magnetic hydrodynamics programs were developed to analyse capabilities of corium direct electric heating. The calculation analysis enabled important conclusions on the identity of heat-and-mass transfer processes in case of DEH and slice geometry/SWH.

The developed CONV-2D and 3D codes take into account the following phenomena:
- convective heat transfer at SWH and DEH of the liquid;
- turbulence of flows at large Rayleigh numbers for which purpose a rather simple model of turbulence was developed;
- model of a transient zone between the solidus and liquidus temperatures in the presence of liquid and solid phases;
- heat conductivity of solid phases taking into account the processes of melting and solidification (crust, structures).

A great attention was paid in the Project to the verification of the computer codes using the data which had been obtained in the experiments with different simulating liquids in rectangular, slice and hemispherical geometries. The results of verification in salt
experiments show that the programs can be used for simulating the processes in a complex 3D geometry. The developed codes were transferred to the project participants for subsequent application.

The use of the codes for pre- and post-test analyses of corium experiments enabled qualitative and quantitative description of the experimental results. The codes are widely used for validating and selecting designs of basic elements of experimental facilities: freezing seals, induction heating system, cooling system. In addition, the programs were used to analyse uncertainties.

8. Corium Experiment Findings in the First Phase of RASPLAV Project

C-22 corium experiments, which had been carried out at different facilities in the first phase of RASPLAV Project, revealed a number of phenomena related to the redistribution of corium density and U/Zr ratio along the height of the ingots (see Figure 28).

It has been found that there is a distinct boundary between the upper and lower layers. The comparison of the obtained results with ZrO$_{0.43}$ - UO$_2$ phase diagram shows that this process has apparently achieved an equilibrium state in RASPLAV-AW-200-1 experiment, while according to RASPLAV-AW-200-2 and TULPAN T3 experimental data it was fixed off at a certain stage which is transient to equilibrium. The analysis of phase diagrams of corium at 2600 °C shows that the process of initial composition separation into two layers might also take place in case of other corium compositions which contain metallic zirconium. The melt stratification process impacts naturally the process of natural convection in the melt pool.

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<table>
<thead>
<tr>
<th>RASPLAV-AW-200-1</th>
<th>RASPLAV-AW-200-2</th>
<th>TULPAN T3</th>
</tr>
</thead>
</table>

Figure 28. Stratification Phenomena obtained in RASPLAV Corium Tests

C-22 corium is a hypereutectic alloy and contains phases in the solid state which greatly differ from each other: uranium oxide with a small addition of zirconium and metallic zirconium with dissolved oxygen. These phases have a significantly different specific weight. According to the literature data, one can assume that for some time after melting corium liquid contains small amounts of phases which correspond to the composition of $aZr(0)$ and $(UZr)O_{2-x}$ phases, resulting in a corium stratification into two liquids.

The transportation mechanisms can be diffusion in a gravitation field and convection in a temperature gradient. Under RASPLAV experiments conditions the convection is apparently controlling factor in the
stratification due to more intensity.

The conducted experiments feature the presence of 0.3 - 0.4 wt% of carbon in corium. According to the experimental results, carbon is primarily dissolved in the light phase which is enriched in zirconium (5 - 6 at%) and it raises up to 25 - 35 % (yellow phase) in a few points. Oxygen and carbon primarily form oxy carbides with zirconium which increases the spreading temperature of light phase. The insufficiency of available data does not allow to exclude completely the impact of carbon on the process of separation - additional experiments are required; however U-Zr-O phase diagrams, which were calculated at 2600 °C by GEMINI code, predict the existence of two immiscible liquids in a rather wide region. Carbon can accelerate the separation process without changing the physical cause of phase separation or fix the light layer during convective movement.

9. Main Results of the First Phase of RASPLAV Project

The implementation of the first phase achieved the principal objectives of the Project. A series of small-scale and supporting experiments, which had been conducted at the stage of studying the feasibility of large-scale experiments, enabled solution of major technical problems. Technology for conducting a large-scale experiment under controlled conditions was developed. This technology is based on the application of the technique of side wall heating of corium and protective layer of materials which prevents undesirable interactions of corium with structural elements. Computation aids enabling pre- and post-test analyses were also developed.

1. A number of physical properties of corium of different composition were measured up to the temperatures of 2850 °C. These properties include: viscosity, heat conductivity, electric conductivity, and melting temperature. The values of the above properties at such high temperatures were obtained for the first time.

2. Two large-scale experiments of RASPLAV-AW-200 series were carried out and important data on the differences in the behaviour of corium melt and simulating liquids in the range of temperatures exceeding 2550 °C under controlled conditions were obtained. All main thermal and physical parameters of the processes at the facilities were controlled and recorded during the operation of measurement systems. The experimental results point to a significant difference in the behaviour of corium and simulating liquids. Liquid stratification and height distribution of U/Zr ratio were established experimentally for different compositions of corium.

3. Five series of experiments were carried out within the scope of the first phase of the Project to investigate melts of different salts as corium simulators. The obtained results allowed understanding of the impact of heating techniques and crust formation on the process of natural convection. These results enabled verification of computation models at high Ra numbers, different heating techniques, presence of transient zone, melting and resolidification of the crust.

4. Analytical and software tools were developed for experimental investigations. The developed models take into account the
most important processes, such as natural circulation of the melt, formation of crusts and their impact on the dynamics, heat behaviour of structural materials, etc. A 3D approach allows the consideration of structural peculiarities of experimental facilities, including real geometry and heating technique. Several additional models, including a mushy zone model, were incorporated into the calculation code. The calculation program was verified using the data of corium experiments as well as experiments with simulating liquids, including experiments which were carried out at the salt facility within the scope of the Project. It was shown that the results of the above experiments were described with a sufficient degree of accuracy which enabled intensive application of the developed computation programs to prepare large-scale experiments.

5. The newly discovered phenomena, which are specific for corium melt behaviour, allowed to generate a number of recommendations for research program in the second phase of KASHLAV Project. The first phase permitted to understand the behaviour of corium, while the second phase should be aimed at studying physical mechanisms which affect the possibility to retain corium in reactor vessel. The second phase is directed at clarifying physical mechanisms which govern corium melt behaviour. In particular, it should help to establish conditions under which stratification occurs as well as mass transfer mechanisms. The database on the properties of materials should be considerably expanded. For this purpose small-scale experiments should be carried out to study individual phenomena as well as integral large-scale experiments.
DEBRIS AND POOL FORMATION/HEAT TRANSFER IN FARO-LWR: EXPERIMENTS AND ANALYSES

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Abstract

The FARO-LWR experiments examine the debris and pool formation from a pour of core melt materials (UO₂/ZrO₂ and UO₂/ZrO₂/Zr) into a pool of water at prototypic accident conditions. The experiments give unique data on the debris bed initial conditions, morphology and heat transfer after the core melt has slumped and (partly) quenched into the water of the lower head. Quantities of up to 170 kg of corium melt are poured by gravity into water of depth between 1 and 2 m through a nozzle of diameter 0.1 m at different system pressures. The debris is collected in a flat bottom catcher of diameter 0.66 m. It reaches heights up to 0.2 m depending on the melt quantity. In general, the melt reaches the bottom only partially fragmented. The debris which forms consists of a conglomerate ("cake") in contact with the collecting structure and overlaying fragments (loose debris). The mean particle size of the loose debris is in the range 3.5 - 4.8 mm. The upper surface of the debris is flat. A gap is present between the cake and the bottom plate. The paper reviews the debris formation and heat transfer to the bottom steel structure from these tests and describes the development of a model to predict the debris and pool formation process. Sensitivity analyses have been performed by the COMETA code to study the behaviour of the ratio between the cake mass and the total mass.

1. Introduction

A light water reactor severe accident may occur due the prolonged absence of water cooling to the core, resulting in core melt relocation to the lower plenum. This was observed in TMI-2 and surprisingly the molten core materials remained in the lower plenum in a stable coolable state. Since that time, experiments and analysis have been focused on the reasons for this and if the mechanisms are robust enough to assure in-vessel retention of a core melt.

The FARO-LWR tests [1] have been designed to provide data on the integral corium melt jet/water mixing and quenching behaviour by using 150-kg-scale of UO₂-based melt in prototypical conditions. Basically, the penetration of molten corium into the water of the lower plenum during a hypothetical core melt down accident and its subsequent settling on the bottom head of the RPV are simulated.

One of the objectives of the experiments is to examine the debris and pool formation from a pour of core melt materials (UO₂/ZrO₂ and UO₂/ZrO₂/Zr) into a pool of water at prototypic accident conditions. The experiments give unique data on the debris bed initial conditions, morphology and heat transfer after the core melt has slumped and (partly) quenched into the water of the lower head (water depth up to 2 m are used). Early experiments examined high pressure conditions (5.0, 2.0 MPa) and recent FARO tests have looked at low pressures (0.5 MPa).

The paper reviews the debris formation and heat transfer to the bottom steel structure from these tests as well as describe the development of a model to predict the debris and pool formation process. The purpose of this model is to take the results of molten core quenching analyses and determine the debris and pool material configuration and thermal conditions as they settle on the lower plenum wall. In addition, the model considers the continued heat transfer from the debris and the pool during heat transfer with the surrounding water and the supporting structure. This model is to be incorporated into the COMETA code which is used to analyse the FARO-LWR experiments.
2. FARO Experiment Description

A typical arrangement of the FARO tests is shown in Fig. 1. The test vessel TERMOs (designed for 10 MPa, 573 K) is connected to the UO₂-ZrO₂ melting furnace via the release channel and isolated from it during interaction by the valve SO2. The test vessel is thermally insulated and connected downstream to a condensing unit via exhaust valves. If during the corium quenching process the pressure in TERMOs reaches the threshold value of the exhaust valves, steam/gas (hydrogen, argon) venting to the condenser occurs.

After melting in the FARO furnace, the melt is first delivered to the release vessel, and then released into the water. Initially, the release vessel is at the same low pressure as the furnace (0.2 MPa) and may contain about 1000 m of 1.2 mm diameter Zr wire (7 kg) uniformly distributed in the whole volume. In that case (L-11 test) the superheated oxide melt coming from the furnace induces the melting of the zirconium and the formation of a homogeneous mixture UO₂-ZrO₂-Zr. After the protection valve SO1 and the isolation valve SO2 have been closed, the release vessel is pressurised with argon to the TERMOs pressure in about 2 s. When the pressures equalise, the two melt catcher flaps (side and melt release flaps, 100 mm in diameter each) automatically open. The side opening prevents against any extra-pressurisation of the release vessel with respect to the TERMOs vessel during the melt release and steam generation. Thus, the melt is delivered by gravity to the water throughout the interaction. The duration of the melt release from the furnace to the water is about 8 s. The melt free fall in the gas space before contacting the water varies from 1 m to 2 m depending on the depth of water. A debris catcher, which is completely immersed in water, collects the corium after the interaction. The catcher consists of a steel bottom plate 40 mm thick with a lateral wall 250 mm high.

The principal quantities measured are pressures and temperatures both in the freeboard volume and in the water, and temperatures in the bottom plate of the debris catcher (Fig. 2). The temperature of the melt is measured in the release vessel by tungsten ultrasonic temperature sensors.

3. Results

The results of 6 tests performed with saturated water are discussed here. Three tests known as L-06, L-08 and L-19 involved respectively 18, 44 and 157 kg of 80 w% UO₂ + 20 w% ZrO₂ melt quenched in 1 m depth of water at 5.0 MPa. One test (L-14) involved 125 kg of 80 w% UO₂ + 20 w% ZrO₂ melt quenched in 2 m depth of water at 5.0 MPa. One test (L-11) involved 151 kg of 76.7 w% UO₂ + 19.2 w% ZrO₂ + 4.1 w% Zr melt quenched in 2.0 m depth of water at 5.0 MPa. In test L-20, 96 kg of 80 w% UO₂ + 20 w% ZrO₂ melt were quenched in 2 m depth of water at 2.0 MPa. The melt temperature ranged between 2900 and 3100 K for the UO₂-ZrO₂ melt and was around 2800 K for the UO₂-ZrO₂-Zr corium. No steam explosion occurred in any of the tests.

3.1. Examination of the Debris

Table 1 summarises the debris characteristics for each test as a function of melt fall in gas space, melt fall in water and system pressure. In test L-11 (UO₂-ZrO₂-Zr corium) the melt experienced complete breakup before settling on the bottom plate. On the contrary, only partial breakup occurred in tests with a pure oxide UO₂-ZrO₂ melt (tests L-06, L-08, L-14, L-19, L-20). In these tests the debris consisted of a conglomerate ("cake" or "hard layer" in the TMI-2 terminology [2, 3]) in contact with the bottom plate and overlaying fragments. The mean particle sizes of the loose debris range between 3.5 and 4.8 mm [4, 5]. Particle size distributions are reported in Fig. 3. The fraction of melt which formed the cake varied from 1/6 (in test L-14, performed with 125 kg of melt and 2.05 m of water) to 1/2 of the total (in test L-19, performed with 157 kg of melt and 1 m of water). The fraction was very similar in tests L-14 and L-20, which differed essentially by the system pressure (5 and 2 MPa, respectively). The cakes correspond to the part of the corium which was certainly still molten when it reached the bottom plate. They are very brittle and break easily during removal.

After each test and prior to any removal of the debris, the water of the debris catcher completely drains out by gravity through 18 holes of 1 mm in diameter drilled at various locations in the catcher for installing thermocouples (8 are drilled in the bottom plate). This indicates that the debris bed is porous, with a highly interconnected pore structure (large effective porosity).

In test L-19, which had a large fraction of debris in a form of a cake, studies of the nature of the debris/bottom structure contact were started. This is one further important issue for evaluating the margin to failure of the lower head. Fig. 4 shows photographs of the cake surface in contact with the bottom plate (test L-19). The surface appears wavy with a furrowed structure. In reality, the cake scarcely contacted the plate. In some regions the depth of the furrows is of several millimetres. Void pockets are present, which might have been created...
by expanding steam upon melt/plate contact. No fundamental difference of the furrowed structure is observed when compared to tests performed earlier in FARO with pure UO$_2$ melts impinging on plates in dry conditions [6]. However, the furrows were less deep and no void pockets were present in the dry tests, and the UO$_2$ which re-solidified on the plate was not so brittle.

3.2. Temperature in the debris bed

In general, the thermocouples located just above the plate within a radius of 240 mm (see Fig. 2) experienced jumps at corium impact. Some were destroyed but others gave signals which remained in their calibration range. An example is given in Fig. 6 for L-14 and thermocouples TD located 10 mm above the plate, at radius 240 mm and two different azimuthal positions (195° and 285°). The temperature reached 1068°C followed by oscillations up to 6 s, i.e. well beyond the corium has spread on the bottom (about 2 s), and indicates that hot corium was still relocating. Later on, the values are similar to those in the water (TW in Fig. 6).

Thermocouples located near the catcher wall (radius 325 mm, i.e., 5 mm from the wall) were generally not damaged, even if located at 10 mm from the bottom. These temperatures are reported in Fig. 7 for test L-19, elevations 10, 40, 82 mm and azimuthal positions 90°, 270°. In this test, the cake covered almost the whole bottom but no thermocouple was damaged. After initial jumps for some of them due to corium contact, they turned to indicate values similar to those in water (TW in Fig. 7).

3.3. Heat-up of the impact plate

In Fig. 8 are shown the temperature increases of the plate contact face at locations where the maximum values were observed. The thermal load in test L-11 (complete breakup of the 151 kg of melt) was negligible with a maximum temperature increase of the plate equal to 20 K. In the other tests, despite the presence of the cake, temperatures remained moderate with a maximum of 850 K measured in test L-14. Neither adherence to nor pitting of the plate have been noticed in any of the tests (this was also the case with the pure UO$_2$ melt/plate impingement tests in dry conditions performed earlier in FARO, although temperatures of the contact face reached 1400 K [6]).

Heat fluxes presented in Fig. 9 have been calculated by using the temperatures of the contact face and those measured in the plate, 5 mm below the contact face. A constant thermal conductivity of 18 W/m.K has been used for the plate. A maximum value around 1 MW/m$^2$ was found for test L-14.

4. COMETA Analysis of Melt Accumulation Rate

The COMETA (Core Melt Thermal-Hydraulic Analysis) [8] code is a coupled thermalhydraulic and fuel fragmentation code conceived for the simulation of fuel coolant interaction and quenching as represented in the FARO test facility; it has been specifically developed to provide a computational tool for test design and specification, definition of operational procedures and test results analysis. COMETA is composed of a two-phase flow field, which is described by 6+n equations (mass, momentum and energy for each phase and n mass conservation equations for n non-condensable gases) and a corium field with 3 phases: the jet, the droplets and the debris. The two-phase field is described in Eulerian while the corium field in Lagrangian coordinates. The two phase flow field is organised in a number of lumped volumes connected with junctions.

A number of sensitivity analyses have been performed by the COMETA code in order to study the behaviour of the following quantity:

$$R_{deb} = \frac{M_{cor}/M_{mol}}{\times 100}$$

This quantity represents the ratio between the cake mass and the total mass in percent. Following an FCI (Fig. 10), if most of the mass poured into the lower plenum is fragmented in particles that quench, a strong energy transfer occurs to the coolant, but a little concern exists for lower head attack by the molten fuel. On the other hand, if the poured mass is partially or even not fragmented at all, a great accumulation of molten fuel could endanger the lower head.

An important set of information on $R_{deb}$ has been provided by the performed FARO tests; excluding the case in which a metallic component was included, all the tests exhibited situations where part of the melt is fragmented and part accumulates as a cake.

The water level, the free fall, the initial pressure, the melt mass discharged to the water and the initial jet diameter influence the relative partitioning. However, in the FARO tests more than one parameter is varied from one test to another. This does not allow identifying the relative importance of the various parameters. Therefore, the COMETA code was used in order to study this problem.

The following range of parametric variations have been considered:
<table>
<thead>
<tr>
<th>Initial Pressure (bar)</th>
<th>Water Level (m)</th>
<th>Free Fall (m)</th>
<th>Injected Mass (kg)</th>
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<tr>
<td>50</td>
<td>0.5</td>
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<tr>
<td></td>
<td>350</td>
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</table>

All the combinations above have been calculated in order to obtain the required trends. These calculations have all been obtained with the fixed TERMOSS geometry in terms of water flow area and gas volume.

The code calculates the three components of the melt: the jet, the fragmented drops and the cake (Fig. 11). As the jet falls down, it fragments into particles in order to satisfy the Jet Break-up Correlations present in the code. The jet component that arrives on the bottom still not completely fragmented accumulates on the bottom and forms the cake. The final value of the cake mass is the focus of this analysis.

4.1. Water level

In this analysis, cases with a fixed injected mass have been considered, namely 177 kg of UO₂/ZrO₂ (Fig.12). Three values of pressure were calculated and 4 values of levels. If the initial water level is below 0.5 m, almost the whole mass accumulates on the bottom as cake, whichever is the pressure. As the water level increases (maintaining the same free fall length), the cake ratio \( R_{deb} \) decreases strongly down to 21% in the case of 50 bar and 2 m water level. This value can be directly compared with the FARO test L-14, in which 16% of the mass was accumulated on the bottom. A case with 1 m water cannot be directly compared with test L-19, because in this test a free fall of 2 m was present. As the pressure decreases the reduction of the \( R_{deb} \) is less pronounced, due to a higher void fraction, which reduces the fragmentation ratio and as a consequence increases the mass accumulation on the bottom.

4.2. Initial pressure

As the pressure decreases the cake ratio increases (Fig. 13). This behaviour is common to the various water levels, even if the extent is different. By comparing the data point corresponding to test L-14 and that corresponding to test L-20, one can note that the calculated behaviour is quite similar to the experimental one.

4.3. Total poured mass

Of particular importance is the influence of the total poured mass, which could allow an estimate of the behaviour with larger quantities of melt. A number of sensitivity analyses has been performed by varying the poured mass in the range 5 to 350 kg with different water levels (1 m, 1.5 m and 2 m). The calculations show (Fig. 14) that the influence of the mass on the ratio \( R_{deb} \) is quite small.

For instance, in the 50 bar - 1 m water and 2 m free fall case, the calculated \( R_{deb} \) is between 25% and 30%. \( R_{deb} \) is very close to experimental values for tests L-06 and L-08 in terms of value and trend, but differs significantly for test L-19, in which a much higher cake mass has been evaluated. It should be noted, however, that the estimation of this mass is not easy, since part of the cake could include particles re-agglomerated in it.

4.4. Influence of free fall length

The influence of the free fall length is presented in Fig. 15. As the free fall increases, the jet reaches the surface with higher velocity and therefore the fragmentation increase is prevailing on the jet elongation, according to COMETA calculations. This trend cannot be deduced by the experimental data since tests are either with 1 m water level and 2 m free fall or with 2 m water level and 1 m free fall. No test was performed with both 1 m water level and 1 m free fall. From the available data, the tests at lower mass show a value similar to the calculated one but the test at higher mass show a value much higher. This parameter could be investigated in the new FAT vessel.

5. Debris and pool formation modelling

In order to better model the FARO-LWR tests, the JRC is continuing to develop the COMETA model to analyze the integral process of corium melt quenching, debris formation and settling as well as molten pool formation. Such an approach also considers the heat loss to its surroundings in the process. The model could also be valuable as a simulation tool for corium pours and associated interactions in LWR severe accident situations, both in-vessel and ex-vessel. We have begun to develop improved models of debris formation and cooling, as well as possible molten pool formation, spreading and cooling. The approach taken in this development is to track the Lagrangian master particles as they settle on the chamber base from
COMETA pool simulations and use this as input for the formation of the debris beds and molten pools. Two eventual situations arise, (1) in-vessel debris/pool formation on a metallic chamber base or (2) ex-vessel formation on a refractory base. Both possibilities are being considered, but we only discuss the former in this work.

For in-vessel debris and pool formation the interaction is divided into two time regimes; an early stage when the melt has contacted the chamber base and is still settling (~10's of seconds) and a later stage when the debris/pool has settled and is cooling due to thermal interactions with the coolant and the chamber base beneath it (~1000's of seconds). The partition of the melt into debris and pool is determined by the internal energy of any master particle group. If the energy is below its solidus point then it is considered debris, otherwise it is considered part of the molten pool. The geometric arrangement is similar to as depicted in Fig. 10 with the debris atop the molten pool; i.e., separated from it by the crust that would develop with continued cooling. The debris bed is considered to give up its energy to the coolant which surrounds and permeates it. The rate of heat transfer from the debris to the coolant pool is determined (1) locally by the film boiling heat transfer from the hot particle to the liquid coolant and (2) globally by the capability of the vapour generated from the local boiling to escape the bed while allowing coolant to enter. The rate of cooling of the debris bed would be given by the minimum of either of these heat transfer mechanisms, with the debris bed thermal state determined by the balance of this heat loss to the heat gain by decay heating.

The molten pool is considered to give up energy to the coolant above as well as the metallic chamber base below. When the molten corium jet is still settling on the base, the stagnation flow of the jet impacting the base will cause the heat transfer to be enhanced. After the pool is settled the heat transfer regime will transition to natural convection to the surroundings. In the first stage we have used a heat transfer model by Epstein and coworkers [9] to describe the stagnation flow and energy transfer down to the chamber base. In the second more quiescent stage of energy transfer we use the work of Jahn and Reineke [10] to predict the natural convection heat transfer coefficient to the chamber base. In both cases the upper heat transfer path is considered to be film boiling from the pool to the coolant with radiation through the film included. Note that any heat loss to the coolant or the chamber base may cause solidification of the pool. In our model representation of this process we consider this to occur as melt freezing as a crust at its boundaries. Also, because of the uncertainties related to thermal contraction, creep and water ingress between the crust and the chamber base, we allow for an interfacial heat transfer resistance to represent these processes. Clearly more work needs to be done to approach this in a mechanistic manner.

To demonstrate the model for the FARO tests we consider the L-14 experiment in which 125 kg of corium melt poured into a high pressure saturated water pool at 50 bar. The model prediction of downward heat flux is shown in Fig. 16. The agreement is qualitatively in agreement with the data from the L-14 test (see Fig. 9) although the interface temperature prediction by the model is higher than the data.

6. Conclusions

The FARO experiments give unique data on the debris bed initial conditions, morphology and heat transfer after the core melt has slump and quenched into the water of the lower head. Tests performed at high pressure showed that, except for the test with Zr metal in the melt, the debris was made of a cake in contact with the bottom and overlaying fragments, which shows similarity with the TMI-2 hard layer and loose debris, respectively. The debris presents a certain drainable porosity. Examination of the nature of the cake/bottom plate contact seems to confirm the hypothesis of a channeling structure as one of the main characteristics which allowed the cooling of the bottom head during the TMI-2 accident. These first examinations will be completed in the future tests by a more quantitative analysis of the debris characteristics (porosity, permeability) and by the use of a shaped debris catcher bottom to better simulate the lower geometrical configuration.

To draw a general conclusion of the FARO tests at elevated pressures, it can be argued that the TMI-2 quenching process during the melt relocation in the lower head and the absence of dramatic consequences for the vessel should not be an exception, at least for the early phase. Whether this statement is still valid at low pressure and with melt including higher percentage of metals will be investigated in future tests.

Understanding the influence of water level, free fall, poured mass and initial pressure on the debris mass ratio ($R_{deb}$) is very important to assess the behaviour of the melt within the lower plenum. A sensitivity analysis has been made by using the COMETA code. It showed that the most influential parameters are the water level and the free fall: changing those parameters strongly changes the ratio. The initial pressure has a smaller effect, but as
the pressure increases the ratio tends to decrease due to less void production. The poured mass, according to the calculations, does not seem to be very important, even if the available experimental data seem not to confirm such statement. New investigations would be needed to have a definite conclusion on this aspect.

The model of debris formation and cooling is still under development and additional comparisons are underway with other FARO data.

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References


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Table 1. Debris characteristics as a function of melt fall and system pressure in FARO tests
Fig. 1. FARO test arrangement

Fig. 2: Distribution of thermocouples in the debris catcher
Fig. 3: Particle size distribution in FARO tests

Fig. 5a: Two different pieces of cake in FARO test L-19 (characteristic size – 50 mm)
Fig. 5b: Same pieces of cake as in Fig. 5a seen from below (part in contact with debris catcher bottom)

Fig. 6: Debris bed temperature in FARO test L-14
Fig. 7: Debris bed temperature in FARO test L-19

Fig. 8: Temperature of the bottom plate in FARO tests

Fig. 9: Downward heat flux from FARO debris beds
Fig. 10: Influence of debris ratio

Figure 11: The melt components calculated by COMETA

Figure 12: Influence of initial water level on the cake mass
Fig. 13: Influence of initial pressure on the cake mass

Fig. 14: Influence of poured mass on the cake mass, free fall 2 m

Fig. 15: Influence of free fall length on cake mass, initial water level 1 m

Fig. 16: Calculated downward heat flux for FARO test L-14
Evaporation and Flow of Coolant at the Bottom of a Particle-Bed modelling Relocated Debris

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Abstract

Subject of the presented work is the experimental investigation of cooling-mechanisms which set in after the relocation of molten debris to the lower plenum during a nuclear accident with core melt. Emphasis is put on the influence of the bottom area of the porous debris. Experiments are carried out with a bed of steelballs (Ø = 2 and 4 mm), heated with an induction coil. The particle-bed is located inside a glass trough with inclining bottom. The particle-bed represents a section from the lower plenum of the RPV. At present, the model-fluid R134a is used and heating rates of up to 560 kW/m² per unit horizontal cross-sectional area of the particle-bed are realized. With these experiments, the evaporation inside the particle-bed and the resulting flow of liquid and gaseous coolant and the influence of an inclining bottom are investigated in detail. The results obtained in the first steady-state experiments and transient experiments are presented and discussed.

1 Introduction

During a core melt accident of a LWR and a relocation to the lower plenum of the RPV, it is of interest, whether or not the RPV-wall can stand the thermal loads of the relocated debris. The history of the accident at TMI-II and the resulting considerations have shown that a substantial internal cooling of the hot debris is possible, if a sufficient amount of liquid is left in the RPV [1, 2, 3]. Consequently, it is of interest to understand the mechanisms removing the initial heat content of the debris and the heat from subsequent decay processes. Assuming that the melt is slowly deposited to the lower plenum, a debris of small particles is formed. This has been shown by many research-programs on melt-coolant-interactions. Such a particle-bed of a porous structure allows the liquid to reach the hot areas. The particle-bed is then cooled by evaporation. The resulting flow of liquid and vapour inside such a porous debris is shown in Figure 1.

Several investigations on the cooling of a cylindrical particle bed with a special focus on dryout phenomena [4, 5, 6, 7, 8, 9, 10] have been performed. It is the aim of the work presented here to get experimental data in a two dimensional particle-bed. The intention is to approach the geometry to the shape of a relocated debris. Taking a section of this particle-bed it can be approximated by a rectangular body with inclined bottom. The liquid supply to the particle-bed is enabled from the upper side of the bottom. This corresponds to the configuration of the particle-bed in the lower plenum where liquid penetrates between RPV-wall and debris coming from the side. The influence of the flow establishing along the bottom area of the particle bed can be estimated.
Steelballs of uniform size are used for the particle-bed. The porosity and the permeability of such a bed of uniform spheres is sufficiently homogeneous which yields to a good reproducibility. The steelballs are located in a glass trough with inclined bottom. For heating a surrounding induction coil is used.

2 Test Facility

The experiments are carried out inside a high pressure test-facility which can sustain pressures of up to 16 MPa. At present, R134a at pressures of up to 1.9 MPa and 66°C is used as test liquid. At this pressure, the ratio of liquid density to vapour density is about the same as it was during the TMI-accident. In future experiments, this test facility will be used for similar tests with water up to 10 MPa and 311°C (TMI-conditions). The test facility consists of a pressure autoclave which contains the glass trough with the model debris. The vapour generated during an experiment is led to a condensor located above the autoclave. The condensed liquid is stored in an electrically heated vessel (feed vessel) above the autoclave. From here the experiment inside the autoclave is supplied with liquid. A heated storage-vessel located underneath the autoclave generates the steam atmosphere and heats the complete facility. This arrangement of the single components allows the circuit of liquid to be driven by natural convection without any pumps.

Inside the pressure autoclave (Figure 2) the particle-bed is located in a glass trough. Different glass troughs with bottom inclination ranging from 5–15° are used. The horizontal cross-sectional area of the particle-bed is 0.42 m x 0.14 m. The trough is made of glass allowing the observation of the evaporation and the flow establishing at the bottom of the model debris. The particle-bed is formed by steelballs of diameter 2 or 4 mm respectively. At the upper side of the particle-bed, an additional ceramic plate can be fixed vertically inside the glass trough. This allows the liquid supply from the bottom of the upper side through the gap between ceramic plate and bottom of the glass trough. This configuration is shown in figure 2. An induction heating, operating at 3300 Hz is used for the internal heat generation. The coil is also located inside the 10 MPa autoclave. The power of the induction heating was measured by heating a dry bed of steelballs. The heating rate can be recalculated from the increase of the temperature. The maximum value realized with the actual induction coil was 560 kW/m² per unit horizontal cross-sectional area of the particle-bed.
For the visual observations, the autoclave has two windows at the bottom. The evaporation and the flow of bubbles are observed with a high-speed video camera. In order to detect pulsating two-phase flow (called chucking), generated during eruptia, quarc pressure sensors are used measuring the pressure changes inside the particle-bed.

Several shielded thermocouples are placed in the particle-bed in order to detect the thermal behaviour of the particle-bed. Furthermore, impedance void probes were designed. They are used to determine of the actual local void fraction during the experiments. These void pobes consist of two opposite electrodes each made from three spheres identical to those in the particle-bed. Each electrode is coated with an insulating layer preventing electric contact with the surrounding steel-balls. This setup ensures that the overall structure of the particle-bed is not disturbed by the probe. The capacitance in between the two electrodes of the void probe is measured with a LCR-meter. The LCR-meter operates prefered at 1000 kHz. Since this frequency is much higher than the 3300 Hz of the induction heating, there is only a small deviation caused by the electromagnetic field. Furthermore, this deviation is reproducible. To obtain the void fraction from the measured capacitance additional calibration of the probe is necessary. The calibration is sone in a bubble column filled with spheres of the same size. The average void fraction inside this bubble column is detected by means of a gamma-densitometer. It was checked that the calibration curves obtained with water can be transfered to other liquids with different dielectric constants and different electric resistance such as R134a. The presupposition is to have two points of the curve, for instance completely filled with liquid and completely filled with vapour.

3 Experimental Results with R134a

Two groups of experiments have been performed. The first group are steady-state experiments where the amount of heat added to the system and the level of liquid above the particle-bed remains constant. They represent a slow cooling process where the generated heat is balanced with the heat removed by evaporation. The second group consists in transient experiments where the particle-bed is dry and superheated beyond the Leidenfrost temperature at the beginning. The hot steel-balls are then flooded with liquid at its saturation point. Fast cooling processes were observed, for instance the change from film boiling tonucleate boiling when the particle-bed is wetted. Within these transient experiments, the heat added by the induction coil can be varied. For both kind of experiments various configurations of the particle-bed were tested, using 2 and 4 mm steelbells, different angles of the glass trough and different heights of the particle-bed. The pressure was varied from 0.8 to 1.8 MPa. Furthermore experiments were carried out both with a ceramic plate at the upper side of the particle-bed allowing an enhanced penetration of liquid at the bottom of the particle-bed, and without ceramic plate where the complete glass trough was filled with steelbells and a liquid supply from the top.

3.1 Steady-State Experiments

In the steady-state test series, the heating rate was increased stepwise up to 560 kW/m². In order to get steady-state conditions, the heating rate was always kept constant for at least 120 sec. Within all steady-state experiments carried out so far, none of the thermocouples indicated a sudden increase of the temperature above saturation temperature which is a typical sign for dryout conditions. Regarding the void fractions measured with the impedance probes, it has to be emphasized that their results are of local validity only. From the calibration-measurements in the bubble column it was observed that the bubbles tend to accumulate in preferred channels. If such a channel of bubbles is passing the two electrodes, the void fraction observed appears higher than the average value. Consequently, the measured void fraction is of a local character only. A void fraction of 1, which means dryout in the particle-bed, has never been observed within the experiments at heating rates of up to 560 kW/m². The maximum void fraction measured with heated steelbells of 4 mm diameter was 0.65 at the top of the particle-bed and a heat-flux of 560 kW/m². A dryout could be reached by increasing the heating rate of the induction coil or by using other configurations of the particle bed.
such as smaller spheres or mixtures of the steelballs with smaller particles.

The recordings of the high speed camera show the generation and the flow of bubbles along the bottom of the glass trough. With the ceramic plate at the upper side of the particle-bed, a uniform downwards directed flow of liquid develops. This flow entrains the generated bubbles. Imediatelly behind the ceramic plate an almost stationary pattern of fingertype vapour channels develops. Primarily at the tips of these fingers bubbles are split of and carried downwards for a while along the bottom of the glass trough. The generation of isolated bubbles and the entrainment can be observed over the complete bottom of the particle-bed. The inclination of the bottom of the glass trough has an influence on the pattern of the finger type vapour channels. For instance for a glass trough with 5° inclination and 560 kW/m² heating rate the vapour channels have a length of 8 times the diameter of the spheres. The overall situation of the flow is sketched in figure 3.

![Flow of liquid and vapour in the bottom area next to the side plate](image)

Despite the irregularities of the flow of liquid and vapour, the quartz pressure sensors did not indicate any pulsations. Chucking, which would cause such pulsations of the pressure inside the particle-bed is expected at higher heating rates in combination with local dryout. These conditions were not reached yet with the experimental facility.

### 3.2 Transient Experiments

For the transient experiments the dry particle-bed was heated up beyond the Leidenfrost temperature with the induction heating. While flooding the hot particle-bed with liquid at saturation temperature, the temperature distribution, the local void fractions, and the establishing flow at the bottom of the trough were observed.

The recordings from the high-speed video system show the liquid front penetrating the particle-bed along the bottom of the glass-trough. This front can be devided into three stages. The first stage is characterized by single drops passing at high velocities in between the gaps of the dry region. While passing through the gaps they evaporate. These drops are entrained from the liquid front which follows with a much lower velocity. In this second stage the steelballs still have a higher temperature than the Leidenfrost temperature. Consequently there is film boiling and the steelballs are not wetted by the liquid. In the film boiling stage the steelballs appear blurred because a thin layer of steam wrapped around them. The droplets mentioned before are entrained out of the liquid front by the steam which
comes from the film boiling stage. When the temperature at the surface of the steel balls falls below the Leidenfrost point the steel balls are wetted by the liquid and nucleate boiling starts. This boiling regime goes on until the steel balls have reached saturation temperature. The front of the third stage follows the front of the film boiling at about the same velocity. Most of the bubbles generated in this stage are ascending through the particle-bed. The observations of the bottom also show bubbles which are entrained downwards along the bottom of the glass trough by the flow of liquid. Figure 4 is taken from a high-speed recording which was taken during the flooding of the hot particle-bed. The three stages of the boiling can be seen. The penetration velocity of the liquid along the bottom can also be obtained from these digital images of the high-speed camera. For experiments with the ceramic plate the flow of the liquid front at the bottom was always directed downwards along the bottom. In figure 5 the velocities of the penetration are shown for a glass trough with 10° bottom. The influence of the inclination from 5°-15° was smaller than the scattering of the measurement. The bottom was always completely wetted before the last thermocouple inside the particle-bed fell under the Leidenfrost temperature.

**Figure 4:** Bottom-view of a section from the particle-bed while flooding at superheated conditions, recorded with HS-camera

When using the configuration of the particle-bed without ceramic plate, the liquid has to enter the model debris from the top. The visual observations of these experiments indicated several locations where the liquid reaches the bottom of the glass trough simultaneously. From these locations, the wet zones expand until the complete bottom is covered with liquid. The visual observations show that the spreading of the liquid processes in the three stages observed before again. As observed in the steady-state experiments, the flow at the bottom shows temporal and local irregularities. It was found that the locations of the first wetting of the bottom and the time needed for a complete wetting of the particle-bed differed from experiment to experiment even if identical initial conditions were used.

**Figure 5:** penetration velocity of the liquid front along the bottom of a glass trough with 10° inclination filled with steel balls of 2 mm and 2 mm, respectively

**Figure 6:** Measured heat flux as a function of the particle-bed temperature while flooding (Ø=4 mm)

As mentioned above the penetration of the liquid appears in three stages:
• single droplets passing the pores at high velocities
• front of film boiling
• front of nucleate boiling

These three stages also differ in the heat flux from each other. Within a cooling process, the heat flux is proportional to the gradient of the temperature following

\[
\dot{q} = -\frac{c m_{\text{sphere}} \partial T}{\pi D^2} \frac{\partial T}{\partial t}
\]

where \(c\) is the specific heat capacity of the steelballs, \(m_{\text{sphere}}\) is the mass of one steelball and \(D\) is the diameter of a steelball. The change of the temperature \(\frac{\partial T}{\partial t}\) is obtained from the measurements of the thermocouples. This heat flux is related to the surface of the spheres. In figure 6 the heat flux, obtained from the temperature measurement, is shown against the temperature. The two curves in this figure refer to two different pressures. The thermocouple was located at the bottom-layer of the particle-bed (\(\theta=4\) mm). Coming from high temperatures the heat flux rises until the first maximum is reached at a temperature of about 160°C. This first rise of the heat flux occurs while the single drops are passing through the particle-bed. The first maximum is reached when the film boiling starts. Like Nukiyama [11] has observed at a horizontal heated plate, the heat flux decreases to a minimum while there is film boiling. This corresponds to the second stage of the visual observations. At this minimum which is also called the Leidenfrost phenomenon [12] the heat flux suddenly increases again. Here the liquid gets in contact with the surface of the steelballs and nucleate boiling starts. Due to the change of the boiling regime the heat transfer coefficient increases suddenly. This third stage of the flooding process produces the highest cooling rates and has consequently the largest heat flux.

From the measurements in particle-beds with spheres of 2 and 4 mm diameter, the temperatures at the moment when the boiling regime changes were selected. They are presented in Figure 7 as a function of the pressure. The saturation temperature of R134a is also shown. In the literature, the temperatures of the Leidenfrost phenomenon are dependent on the orientation and geometry of the hot surface, on the material, and on the structure of the surface. The results presented here have a wide scattering of temperatures which indicates the local variations of the flow. Also the irregularities inside the particle-bed are affecting the flow of coolant. Within the accuracy of the measurement the size of the steelballs have no influence on the temperature where the boiling regime changes.

![Figure 7: Temperature at Leidenfrost point in spherical particle-beds compared with saturation temperature. (For technical reasons measurements were performed around 0.8, 1.2, 1.5 and 1.8 MPa)](image)

Regarding the heat flux at the Leidenfrost temperature while there is still film boiling, there is a dependency on the particle diameter (Figure 8). The particle-bed with smaller spheres has a bigger ratio of surface over volume. Consequently the ratio of heat over surface is smaller, which results in a smaller heat-flux.

Comparing the maximum heat flux which occurs while there is nucleate boiling, the larger spheres provoke a higher heat flux again (Figure 9). This maximum heat flux is called the critical heat flux. In this case, the ratio in between the results using 4 mm spheres and 2 mm spheres is larger, compared with the heat flux at the Leidenfrost point. The permeability which is related to the size of the pores inside the particle-bed depends on the size of the spheres. Consequently, the 4 mm particle-bed allows a better flow of liquid towards the hot zones and a better ascending of the generated vapour bubbles. This coincides with the measurements of the velocities of the liquid, which enters at the bottom of the particle-bed during the flooding experiments. The velocities measured in a particle-
of the bottom of the glass trough and by comparing the time the last thermocouple was indicating nucleate boiling.

4 Discussion and Conclusions

In the literature, most of the experimental work investigating evaporation and the resulting two-phase flow in particle-beds concentrates on the determination of dryout conditions. Several semi-empirical models developed on this experimental data can be found [5, 7, 8]. Within these models the terms taking the permeability under account are solved with the help of experimental data. Based on these fundamental models one and two-dimensional models were developed not only calculating the dryout conditions but also the overall liquid distribution [4, 6, 9, 10]. The zero and one-dimensional models from Catton, Lipinski and Ostensen predict a heat flux density of 203 to 787 kW/m² (related to the cross section of the particle bed) when using steelballs of 2 mm diameter in R134a at 1.5 MPa. When using 4 mm diameter steelballs, the predictions are in between 287 and 1107 kW/m². With a heating rate of 560 kW/m² generated within the here presented experiments the particle-bed did never reach the dryout conditions at any location. Neither a liquid supply from the top nor a liquid supply from the bottom could enforce the dryout. Consequently, the transferability of these models to the here investigated two-dimensional geometry with inclining bottom could not be shown. These models are based on experimental data which were obtained with elementary geometries such as cylinders. For more general geometries it is necessary to adapt these models or to develop more complex models also taking into account the observed irregularities of the flow during steady-state experiments. Both, the transient experiments, and the steady-state experiments indicate that the flow of coolant depends on details of the particle-bed.

The way the coolant is supplied has also a strong influence on the establishing flow. For instance, for the geometry with the plate at the upper end of the particle-bed and the resulting inflow of the liquid near the bottom the flooding of the debris is much
faster compared to the geometry without the plate, and a coolant input from the top.

When looking at a hot relocated debris at the lower plenum of a RPV, it is important to determine the exact amount of heat the RPV-wall is exposed to. It has been observed that an enhanced flow of coolant to the bottom of the hot particle-bed from the side provokes a flow along the bottom of the particle-bed. This results in a better supply with liquid coolant. The tests carried out so far show that the results from experiments with a one-dimensional cylindrically shaped particle-bed can not necessarily be transferred to two- and three-dimensional geometries.

It is the aim of this ongoing project to gain a better understanding of possible cooling mechanisms and to obtain experimental data which can be used for the development of numerical models. At present the experiments are carried out with the refrigerant R134a as test liquid since instrumentation makes no difficulties. For a better transferability to nuclear accident conditions test series with water as test liquid at up to 10 MPa are planned later.

References


OECD/CSNI Workshop on In-Vessel Core Debris Retention and Coolability

Investigations on the Coolability of Debris in the Lower Head with WABE-2 and MESOCO-2D

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Abstract

Analyses on external cooling of a melt pool in the lower head of the reactor pressure vessel (RPV) or of gap cooling inside the RPV means to consider limiting configurations for coolability. However, before running into such configurations, the formation of particle agglomerations (debris) can be expected in the most relevant scenarios. Analysis of such configurations shall help to identify conditions to avoid such limiting cases, to assess the available time spans to reestablish coolability by water injection and to evaluate constructive or procedure means to support it.

The potential of long term coolability of debris beds can in principle be investigated by the modules WABE-2D and MESOCO-2D developed at IKE in the frame of the KESS code system for implementation in ATHLET-CD. WABE-2D describes separated two phase flow of water and steam in a debris bed submerged in a water pool under various boundary conditions of coolant inflow (gravity induced inflow from an overlying water pool yielding counter-current flow of water and steam in the bed or imposed inflow from the lateral boundaries respectively the bed bottom result in co-current flow situations). Coolant boil-off, dry zone formation and extension in the bed are considered by WABE-2D. If a sufficient coolant supply can not be established the dry zone will expand up to total bed dryout and temperatures will rise up to particle melting. The thermal non-equilibrium between particles, liquid and steam is taken into account by WABE-2D. This extends the range of coolability investigations and enables to analyse quenching of hot debris beds.

For the simulation of melting, melt relocation and refreezing including melt pool formation on crusts in a porous solid matrix the module MESOCO-2D has been developed. Gravity as well as capillary forces are taken into account for the melt flow. Forced and natural convection gas flows are also described in 2D. Thermal non-equilibrium between solid matrix, melt and gas is taken into account.

Up to now WABE-2D and MESOCO-2D have been developed as separate modules. Both modules will be combined to an integrated model for safety related analyses to evaluate conditions for a long term coolability as well as the safety margin to vessel failure. The effectiveness of cooling mechanisms and accident management measures to prevent RPV failure can be checked.

1 Introduction

In the course of the TMI-2 accident larger masses of melt (20 t) were relocated from the core into the water filled lower head of the RPV /1/. There the core material formed a particulate bed of fragments and crusts with a melt layer above. The results of the TMI-2 Vessel Inspection Program (VIP) /2,3/ revealed that an elliptical "hot spot" located near the center of the lower head showed a severe thermal history up to 1050-1100 °C for about 30 minutes.

The cooling mechanisms which caused a cooldown to 700-800 °C and prevented vessel failure, e.g. heat removal due to coolant circulation in a gap between the debris and the RPV wall as well as the heat removal capability from the debris are currently under investigation in the frame of several experimented and analytical research projects.

Currently, in the frame of two projects sponsored by BMBF the in-vessel phenomena during late phase core degradation in Light Water Reactors (KESS-
project) and the cooling mechanisms in debris beds during quenching (DEBRIS-project) are investigated at IKE. The KESS /4.5/ code simulates the relevant processes during the early, transition and late phases of a core meltdown accident. Up to now most of the KESS modules have been implemented in the German system code ATHLET-CD. The modules for the simulation of the central processes during the later phase of core degradation are:

- **WABE-2D (Water bed) /4,5,6/:** coolant boil-off in severely damaged core regions as well as in debris beds in the lower head of the reactor pressure-vessel (RPV), quenching of hot, dry debris beds and degraded structures
- **MESOCO-2D (Melting and Solidification of Core Material) /4,5,6/:** melting and melt relocation, refreezing, crust and melt pool formation in a degrading core and in debris beds
- **IKEJET/IKEMIX (Jet breakup and mixing) /5,7/:** melt relocation from the core into the water-filled lower head of the RPV: interaction of the melt jet with a coolant, fragmentation processes, formation of a debris bed.

The examination of the melt relocation modes and the fragmentation processes (IKEJET/IKEMIX) gives important information on the possible mass, composition and debris size distribution of the relocated core material in the lower head. This paper concentrates on the discussion of the modules WABE-2D and MESOCO-2D.

The objective of the DEBRIS-project is the refinement of the model WABE-2D. The modeling work is supported by model orientated experiments at IKE from which improved relation for the exchange terms (friction between debris and a steam/water flow, heat transfer during quenching of hot dry debris) will be deduced.

In the literature the extreme case of an extended melt pool in the lower head is discussed. This configuration represents an ultimate case which must be avoided due to its difficult coolable state. During the evolution starting from debris bed formation (loose debris, crusts) in the water filled lower head via dry zone formation up to total dryout and formation of an extended melt pool several coolable configurations may be possible.

The modules WABE-2D and MESOCO-2D are able to simulate the relevant processes which determine the coolability over a large variety of such configurations. The model features of WABE-2D and MESOCO-2D are summarized briefly. Calculations for model validations as well as results of cases with inhomogeneous debris beds (regions of smaller porosity and higher decay power) are presented. These results reveal 2D-effects with self regulatory cooling mechanisms which help to stabilize dry zones in a boiling bed and may support long term coolability.

2 Model Features of WABE-2D

WABE-2D is designed to simulate the coolant boil-off in a debris bed submerged in a water pool as well as quenching of hot dry debris in r,z- or x,y-geometry /5,6/. The model is based on a quasi-continuum approach. During coolant boil-off co- or countercurrent vapor and liquid flow in the bed, dry zone formation and extension are considered. The dry zone will extend up to a complete bed dryout if sufficient coolant supply for heat removal cannot be established anymore. Then the extending dry region will heatup due to less efficient heat removal mechanisms (heat conduction, radiation and single phase heat transfer). For the description of the processes transient conservation equations for mass (liquid, vapour) and energy (liquid, vapour, particles), steady state momentum equations according to Ergun /8/ and an additional equation to calculate local pressure differences between the liquid and vapor phase due to capillary effects are used.

Because of the three separate energy conservation equations thermal non-equilibrium between the particles, liquid and vapor can be taken into account. This extends the range of investigations of WABE-2D and enables the model to analyse quenching of hot debris. However, up to now constitutive relations for momentum and energy transfer are used which are restricted to debris beds composed of spherical particles with locally uniform diameter:

- the pressure loss of a liquid/vapor flow in the debris bed is calculated with an empirical momentum equation for each phase according to Ergun /8/, which has been modified by relative permeabilities and passabilities,
- capillary pressure which may induce a liquid flow along a liquid fraction gradient (liquid fraction: volumetric fraction of liquid in the pore
spaces) is taken into account by the relations of Leverett /9/ and Reed /10/.

- in the present version of WABE-2D heat transfer only between the particles and the coolant is considered. Liquid and vapor phase are assumed to be in thermal equilibrium. At present heat transfer coefficients according to relations of nucleate and film boiling heat transfer are used.

Currently at IKE a test facility (system pressure up to 4 MPa) is under construction in which model orientated experiments will be performed to deduce improved expressions for the pressure drop of water/steam flows in a inductively heated particulate bed as well as particle-to-coolant heat transfer data during quenching of hot dry debris. Different particle diameters in the range between 2 and 6 mm will be considered.

3 Comparison of WABE-2D Results with Data on the DCC-2 Test Series

For a first check results of WABE-2D have been compared with experimental data of the DCC-2 test series (Degraded Core Coolability, test series 2) /11/. The test series were performed in the Annular Core Research Reactor (ACRR), Sandia National Laboratories, with a debris bed (height H = 0.5 m, bed diameter D = 0.06 m) compared of UO₂-particles of 1.42 mm in diameter and an average porosity of ε = 0.41. The debris bed is located in a cylindrical crucible. Both, the lateral walls and the bottom plate is adiabatic an impermeable. Liquid enters the bed from an overlying coolant pool (Fig. 1).

The local heat generation rate in the debris bed is a function of axial location and local liquid fraction. A

Figure 1: Sketch of the DCC-2 test setup: debris bed with overlying coolant pool.

Figure 2: Comparison of axial liquid fraction profiles calculated by WABE-2D, PAHR-2D LWR and ENTH, DCC2 test (p=0.62 MPa, d = 1.42 mm, ε=0.41, H=0.5m, D=0.06m).
relation between the reactor power (ACRR) and the local heat generation rate is given in /12, 13/. At the beginning (t = 0 s) of the considered test the pore spaces are filled with liquid. The liquid and particle temperatures are adjusted to the saturation temperature of the coolant corresponding to a system pressure p of 0.62 MPa (thermal equilibrium condition). The reactor power (ACRR) is switched to P = 255 kW. After a steady state bed condition has been established a power ramp up to P = 625 kW (ACRR) was carried out.

The experimental conditions (saturation temperature, limited power steps) of the DCC-2 tests support the assumption of thermal equilibrium between particles and the coolant. The assumption is confirmed by the good accordance between experimental data (dryout heat flux, location and time of dry zone formation) and already performed model calculations, e.g. that of Brealey /12, 13/ (ENTH-code, 1D, transient, thermal equilibrium) and Mayr et al. /14/ (2D-code PAHR-2D LWR, transient, thermal equilibrium, r,z-geometry).

To compare the already existing data with the results of WABE-2D (thermal non-equilibrium model), a heat transfer coefficient of α = 10 W/m²K was assumed to balance temperature differences between the particles and the coolant within a short time period. In Fig. 2 the liquid fraction profiles calculated by WABE-2D, PAHR-2D LWR and the ENTH-code for the steady state bed condition (P = 255 kW ACRR) and the axial liquid fraction development in dependence of time up to dry zone formation are shown. For P = 255 kW the steady state condition was reached within 5-6 s by all models. The liquid fraction profiles are in good accordance with each other. One hundred seconds after the power ramp was carried out (P = 255 → 625 kW) a dry zone (s = 0, 0.16 < z < 0.21 m) was detected during the experiment /11/. The results of PAHR-2D LWR and of ENTH (only the calculated dry zone extension is shown) are in quite good agreement with the observed dry zone extension. At 100 s a liquid fraction profile without dry zone was calculated by WABE-2D, however, the liquid fraction minimum (s = 0.08) is located at the same bed height where the dry zone formation was detected during the experiment. The different results are currently under investigation. Nevertheless, in view of the presently existing uncertainties this first check demonstrate reasonable results of WABE-2D which are in good accordance with the other models.

4 Influence of 2D-Effects on Debris Bed Coolability

To investigate the influence of 2D-effects on debris coolability calculations with WABE-2D on test cases were done. For the analyses a debris bed with a diameter D of 1 m and a height H of 1 m was examined. As a base case a debris bed with a uniform porosity (ε = 0.5), particle diameter (d = 1 mm, UO²) and specific power density (q = 160 W/kg UO², 0.8 MW/m²) was assumed. The specific power was raised from q = 0 (t = 0 s) to 160 W/kg linearly within 50 s and was then kept constant. The test arrangement is comparable to that shown in Fig. 1. 2D-effects were considered by assuming a debris bed with a

- locally reduced porosity of ε = 0.3 (case 1)
- case 1 + radially decreasing specific power density (case 2)
- lateral coolant influx (case 3).

The analysis of the base case reveal a typical 1D behavior. After t = 50 s at constant bed power a uniform counter-current flow regime has been established. Due to intensive vapor generation the inflowing liquid from the overlying coolant pool is hindered. With increasing time the local liquid fraction in the bed decreased gradually. After 200 s a dry zone has formed extended over the whole bed radius. Particle temperature raised and the dry zone expanded up to a total bed dryout.

The analysis of the debris bed with an assumed locally reduced porosity (ε = 0.3) inside the area 0.0 < r < 0.5 m, 0.45 < z < 0.6 reveal a different behavior (case 1). Due to a larger solid fraction the power density is about 40 % larger in the denser part of the bed compared to the base case condition. In addition, smaller pore sizes respectively a reduced cross sectional area for liquid and vapor flow induce larger pressure loss which locally deteriorate coolability. In Fig. 3 the liquid fraction and liquid velocity distribution (upper row) as well as the particle temperature and vapor velocity distribution (lower row) are shown at t = 50, 100 and 200 s. The scales (upper and lower row) represent the local liquid fraction and particle temperatures.
Figure 3: Calculated liquid fraction and liquid velocity (upper row) as well as particle temperature and vapor velocity distributions (lower row) at times $t = 50$, 100, 200s (case 1, $H = 1m$, $D = 1m$, $\xi = 0.5/0.3$, $d = 1mm$, $p = 0.1MPa$).
At $t = 50$ s liquid and vapor are flowing upwards because the specific power density is increased up to 160 W/kg UO$_2$. Generated vapor which cannot escape fast enough through the pore spaces pushes liquid out of the bed. The vapor pressure is even high enough that liquid is pushed out of the denser part ($r = 0.3$) towards the bed bottom. At $t = 100$ s the whole area of reduced porosity and a smaller region below is dried out.

Above the dry region the vapor pressure is reduced due to a reduced vapour production from below. So, liquid influx from the overlying coolant pool (see Fig. 1) is facilitated. The locally increased liquid supply and liquid flow around the dry zone stabilize its upper and lateral boundary. Vapor generated in the lower part of the bed is flowing upwards around the dry zone. This explains a laterally and upwards directed expansion of the dry region ($t = 100, 200$ s, upper row). The difference in liquid fraction between the inner and outer part of the bed induces a hydrostatic pressure difference which enhances lateral liquid inflow and help to stabilize the lower boundary of the dry zone.

In comparison to the base case (uniform bed structure) a lateral dryout of the bed does not take place. On the one hand the inhomogeneity cause a locally reduced coolability. On the other hand coolability enhancing mechanisms in the global behavior, like the formation of separate flow paths of escaping vapor and inflowing liquid (e.g. above the dry zone) cause a reduced pressure loss of each phase which support the global coolability of this configuration. If sufficient liquid is available a sustained stabilization of the dry region and a long term coolability may be possible.

An even more pronounced effect to stabilize the dry zone was observed in the case with an assumed radially decreasing power density, case 2 (Fig. 4). The geometry and the bed data are identical, the average bed power is comparable to case 1 (s. Fig. 3). Due to the radially decreasing vapor generation rate the pressure loss during liquid inflow is even lower, especially in the outermost bed region. This favors a higher availability of coolant for heat removal from the dry zone and improves the global coolability of the bed. Although higher particle temperatures (due to a larger power density in the innermost region) were calculated in comparison to case 1, the extension of the dry zone remains nearly the same.
Depending on the location and geometrical configuration of the debris bed in the RPV (Fig. 5), a liquid inflow is possible from the lower or lateral boundary. The lateral coolant inflow (Fig. 5, right) is induced due to hydrostatic pressure differences between the surrounding liquid pool and the coolant column in the boiling debris. Such influxes result in a co-current flow of liquid and rising vapor and will affect the pressure loss for each of the phases.

In a test case (geometric and bed data of the base case) a given lateral coolant inflow near the bed bottom was considered. Several liquid mass flow rates up to $m = 5$ kg/m/s were assumed. After dryout the lateral coolant inflow effects a limited extension and stabilization of the dry zone. Due to a higher coolant availability inside the bed, even a small reduction of the dry region was observed with increasing time (in the case of $m = 5$ kg/m/s). A lower pressure loss favors the accessibility of the coolant within the whole bed and enhances its global coolability.

5 Late-phase melt progression model

MESOCO-2D

If a sufficient coolant supply cannot be established anymore, a dry zone may extend up to a total bed dryout. Particle temperature will raise up to melting. MESOCO-2D describes solid heatup, melting, melt relocation, solidification and melt pool formation on a crust, remelting and crust failure in a degrading core as well as in a debris bed in the lower head. A detailed analysis of these processes is necessary to evaluate the time span during which configurations develop which remain coolable (to be checked with WABE), with the extreme case of a melt pool in the lower head, and finally the time available up to RPV failure. These times give margins for initiating additional cooling measures. Further, the analysis of the development of the debris bed states gives a basis for evaluating the kind of limitations to coolability occurring before the melt pool case will be reached.

The relevant processes are modelled in $r,z$- or $x,y$- geometry. Transient conservation equations for mass and energy are used for each of the phases (solid, melt and gas). Thermal non-equilibrium between the solid matrix, melt and gas is taken into account. Melting and freezing rates are determined from the net heat flux from solid and liquid into the phase interface divided by the latent heat of fusion. For heat transfer between solid and interface, heat conduction is assumed. Heat transfer between interface and liquid is calculated according to correlations for forced convection of melt flow.

For simplification, quasi-steady state approaches are used for the momentum conservation equations of melt and gas. The Ergun formulation of friction pressure loss is used. Gravity, hydrostatic pressure differences as well as capillary pressure are taken into account as driving forces for the melt flow. Melting and refreezing is described for 3 material components, a metallic U-Zr-O composition as well as the ceramic components $UO_2$ and $ZrO_2$. For debris beds with metallic Zry constituents the exotherm Zry/H$_2$O-reaction is modelled. The hydrogen production as well as the energy release due to reaction enthalpy are quantified and hydrogen is taken into account in the gas phase by means of a separate mass conservation equation. Conduction as well as radiation (particle-particle, debris-surrounding structures) are modelled for the solid. Heat transfer to the gas is described locally by means of forced convection correlations. Like in the WABE-
2D model the constitutive relations are presently restricted to debris configuration with spherical particles and locally uniform diameter. The implementation of relations for energy and momentum transfer for debris beds consisting of fragments of different size and shape, crusts and blockages is intended in the future development.

6 Pre-test analysis of PHEBUS FPT-4 with MESOCO-2D

For the layout of the planned PHEBUS FPT-4 experiment at the Research Centre Cadarache (CEA) an international effort was initiated. The aim of the FPT-4 test is the investigation of fission product release from severely damaged core configurations including debris and melt pool upon a crust. The test will be carried out in a setup consisting of a debris bed composed of a mixture of UO₂ and ZrO₂ particles (mass relation 80/20, total mass 6 kg, porosity 50%, average particle diameter 4 mm). The debris bed will have a diameter of 6.8 cm and a height of 36 cm. The bed is surrounded by a liner and a shroud with several layers of insulation material to protect the setup against melt attack and to avoid heat losses. A detailed description of the planned test setup is given in [15]. From below the bed is cooled by a gas flow (steam or steam/hydrogen mixture) which also acts as a transport medium for released fission products.

Pre-test calculations with different codes (ICARE (CEA), DEBRIS, MERIS (SNL) and MESOCO) have been performed to support the experimental team in the layout of the experiment. Main aims of these calculations were to predict the amount of molten material and the phenomenology of melt relocation (position and downwards and lateral progression of crust/melt pool configurations) depending on the experimental conditions (e.g. debris bed composition, heating power and gas mass flow rate and composition). These data can be used to prevent potential damage of the test setup and to find adequate stopping criteria e.g. based on shroud temperatures.

For the MESOCO-2D calculations the debris bed, the radial shroud as well as part of the lower structures (thoria insulation, steel plate) have been modeled. For the top surface of the bed, radiation against the upper structures (at steam saturation temperature), which have not been considered, has been assumed. For the material interactions between UO₂ and ZrO₂, a simplified approach has been applied in a first step. Melting and refreezing of UO₂ and ZrO₂ first takes place at the melting temperature of the eutectic point (~2870 K) and with eutectic composition (~48 Mol% ZrO₂) according to the quasi-binary UO₂-ZrO₂ phase diagram. After consumption of the available ZrO₂, the remaining UO₂ melts at the melting temperature of UO₂. A more detailed modeling of UO₂-ZrO₂ material interactions based on phase diagrams is underway, but was not yet available for the calculations.

A uniform specific power distribution in the bed has been assumed. The total bed power was raised by several steps (calibration plateaus) to a final value of 20 kW after 4000 s. A constant mass flow rate of 0.8 g/s of gas composed of 20 Mol% hydrogen at 440 K was assumed at the bottom. First calculations with MESOCO-2D have been carried out with the assumption of thermal equilibrium between melt and solid (forced by very large heat transfer coefficients of 10⁶ W/(m²K) between melt, solid and phase interface).

Due to the cooling by the gas flow, prior to first melting a strong axial temperature profile exists in the bed in addition to the radial temperature profile imposed by heat losses to the shroud. Melting therefore first starts along the centerline in the uppermost quarter of the bed at ≈ 4200 s. The melt relocates downwards, increasing on the one hand the temperatures in lower regions. On the other hand, it refreezes, forms a region of reduced porosity and later densifies to a crust. The downwards progression is slowed down strongly due to the axial temperature gradient. As more melt is produced and flows downwards, it accumulates on the crust and begins to form a melt pool. Due to the hindered downwards progression of the pool/crust configuration the pool is forced to extend radially.

Finally a configuration is reached in the calculation with a melt pool extending over the whole cross section, located axially approximately in the middle of the bed. This corresponds to similar results obtained with other models (ICARE, DEBRIS, MERIS). This behavior predicted by the models poses several problems for the experimental layout. On the one hand, the melt may attack the radial shroud, and, on the other hand, the gas flow is blocked by the crust/melt pool.
Figure 6: Results of MESOCO-2D pre-test calculation on PHEBUS-FPT4 considering thermal non-equilibrium between melt and solid at times $t = 4750$ s (a) and $4800$ s (b).
A further calculation has been carried out with MESOCO-2D assuming now thermal non-equilibrium between solid and melt. Major effects of thermal non-equilibrium are that melt may more easily overheat, especially when accumulated in a pool which still includes solid parts, and that melt can penetrate further downwards into colder regions before it completely freezes. Therefore, additional effects can be expected in contrast to the calculations with thermal equilibrium.

Fig. 6a shows the results for the calculation at 4750s. Up to this time results are similar to the former case with thermal equilibrium. Significant melt masses have been produced and have accumulated in a melt pool on a crust. A voided region has formed above the pool. Due to slowed axial relocation the melt pool has extended radially, forming the "nose" in the porosity profile. However, the accumulated melt in the pool now is significantly overheated (~150K).

The results of Fig. 6b at 4800s show that further heating and melt accumulation lead finally to a failure of the lateral crust. The outflowing overheated melt has remelted parts of the lateral crust and has relocated downwards, forming a densified region in the outer region of the bed after refreezing. Only a smaller part of the melt remains in the melt pool in the center. Thus, lateral extension is at least partly and temporarily stopped due to superheating of melt and subsequent pronounced downwards flow after lateral crust failure.

7 Summary

WABE-2D and MESOCO-2D describe the relevant processes during coolant boil-off, dry zone formation and extension up to total bed dryout as well as melting, melt relocation, crust and melt pool formation. Both modules have been developed at IKE as separate modules. Due to a lack of experimental data WABE-2D calculations were carried out with assumed test cases to investigate 2D-effects (e.g. region with reduced porosities, lateral coolant influx) on dry zone extension and coolability. The results reveal interesting countering tendencies in the debris bed behavior. Despite of local enhanced vapour generation (due to heigher power density, reduced porosity) and dryout self regulatory mechanisms support the global coolability of the bed. If sufficient coolant is available a sustained stabilization of dry zones and a long term coolability of the whole bed may be possible. Further calculations are necessary to investigate the cooling mechanisms.

The processes referring melt propagation, crust and melt pool formation are described by MESOCO-2D. Results of calculations for the planned PHEBUS FPT-4 experiment show that cooling from the bottom can significantly slow down axial melt relocation. Melt accumulation leads to a counterplay between lateral and axial extension of the melt pool, depending on the cooling conditions. Such effects in general determine the temporal behaviour of melt/crust configurations. They finally give the initial conditions, e.g., for the configuration of a large pool in the lower head and thus determine the conditions for coolability. The calculations on FPT-4 in addition show, that melt superheating can lead to additional effects, delaying at least temporarily the extension of melt pools and favouring subsequently, after crust failure, axial melt progression with distribution of melt over larger regions. Further model improvements will be necessary to be able to extrapolate the model results to realistic conditions.

Using WABE-2D and MESOCO-2D an assessment of coolability for possible debris configurations in the lower head is possible. Both modules will be combined to an integrated modul for improved analysis, especially for considering the influence of molten parts on the coolability during water injection. Furthermore, analyses will be performed on the effectivity of cooling measures and the available time spans until the limiting case of a melt pool will be reached.

Beside the integrated model development future work will concentrate on the refinement of constitutive relations for momentum and energy transfer. In 1998 model orientated experiments (up to 4 MPa system pressure) will be carried out at IKE. Debris beds with particle diameters in the range between 2 and 6 mm will be examined. For the evaluation of improved relations for the two-phase pressure drop different flow regimes will be adjusted in the bed. In another test series heat transfer relations during quenching of hot debris will be deduced. In a first step bed temperatures up to 1100 °C will be examined.
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Uncertainty and Sensitivity Analysis of the Heat Transfer Mechanisms in the Lower Head

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Abstract

This paper illustrates a comprehensive approach to investigate the heat transfer mechanisms between relocated core debris and the RPV wall, to quantify the modelling uncertainties and to identify the most sensitive parameters or models involved. The investigations are mainly oriented to the TMI-2 conditions. Moreover, a high Rayleigh number scenario has been investigated.

Previous simplified simulations with the thermohydraulic code ATHLET confirmed that a comparatively small coolant flow passing through a gap between lower crust and the vessel wall can reproduce the thermal history of the TMI-2 vessel during the accident.

A refined integral model for the interaction between debris and RPV wall has been developed which covers the major physical phenomena governing the long-term integrity of the RPV. An uncertainty and sensitivity analysis of this model has been performed which accounts for modelling and parameter uncertainties. Quantitative sensitivity statements were derived for the code results accounting for the combined influence of these uncertainties as well as for the possible impact of the sampling error.

1 Introduction

The knowledge of the various phenomena which occur after a relocation of core material to the lower plenum of the RPV is of major importance for the safety analysis of severe accidents, providing the initial conditions for evaluating the possibility of confining the accident consequences inside the reactor pressure vessel, or for determining the vessel failure mode and the release of core material into the containment. The validation of severe accident models is limited since it is not possible to simulate a late phase accident sequence in experiments at or near full scale. The results of the TMI-2 investigation programme even though they are restricted to the specific sequence of this accident remain the only reference reactor data for assessing the capabilities of present analysis tools [1].

The aim of the work presented in this paper is to develop an integral model of the heat transfer mechanisms in the lower head after a relocation of core material as well as of the structural response of the pressure vessel. Furthermore, uncertainty and sensitivity analyses have been carried out at a comparatively early stage of model development, providing valuable insights in the sensitivity of safety relevant parameters to the various interrelated physical phenomena and processes in the lower head. They also help to understand the shortcomings in current modelling, to identify
the main points for allocating research resources, to improve the robustness of the models, and to develop a structured approach of how to assess and to deal with the uncertainties of predictive models for severe accident simulations.

Due to the lack of experimental data the analyses are mainly oriented to the TMI-2 accident. During this accident, about 20 tons of core material relocated to the lower head. An internal cooling mechanism due to material creep of the pressure vessel and water ingestion into the expanding gap between the debris and the vessel wall provided a conclusive explanation how the debris in the lower head was finally cooled and how the integrity of the pressure vessel was sustained in the course of the accident [2].

In previous investigations a simulation model for the lower head was developed with the GRS thermohydraulic code ATHLET [5]. In this simulation the molten corium in the lower plenum was represented by a simplified lumped parameter model. The two-phase flow in the gap between debris and vessel wall was simulated by an one-dimensional channel (Fig. 1). The calculations confirmed that a relatively small flow rate of 0.2 kg/s (Fig. 2) is capable of reproducing the thermal behaviour of the vessel during the accident which had been determined by metallurgical studies in the TMI-2 Vessel Investigation Project [3]¹.

![Diagram of the ATHLET model for the heat transfer in the lower head during the TMI-2 accident](image)

**Figure 1** ATHLET model for the heat transfer in the lower head during the TMI-2 accident

![Graph showing temperature in the hot spot region](image)

**Figure 2**: Vessel temperature in the hot spot region

¹ These studies revealed that a small region (hot spot) of the TMI vessel had reached a surface temperature of approximately 1400 K for about 30 min and then experienced a relatively rapid cooldown with a gradient of 10-100 K/min, whereas the temperature outside this region did not exceed the ferritic-austenitic transition temperature of about 1000 K.
2 Modelling approach

The considered debris configuration, i.e. a melt pool with overlying particle bed, is based on the conclusions of the TMI-VIP [4]. Moreover, the estimation of the dryout heat flux of a particle bed corresponding to the total mass drained to the lower head with, for example, the Lipinski model [6] reveals that dryout and thus a formation of the melt pool out of a particle bed cannot be expected for the TMI-2 situation.

Presently, the heat transfer model of the lower plenum consists of two parts, first the AIDA module (Analysis of the Interaction between Core Debris and the RPV during Severe Accidents) which is being developed for the SA code ATHLET-CD, secondly the finite element models HEAT2D and PSU\(^2\) for heat conduction in the crust and the wall (Fig. 3). AIDA delivers the time-dependent heat flux distributions as input for HEAT2D and PSU. HEAT2D is currently coupled with AIDA.

AIDA is an integral model for the behaviour of core material resting in a certain configuration. The initial conditions (e.g. melt mass, composition, temperature) have currently to be defined by input. The natural convection heat transfer from the melt pool to the crust is described by the usual Nusselt-Rayleigh number correlations (7 correlations available for the lower hemispherical boundary, 4 for the upper circular one. The heat conduction in the melt pool crust is calculated with a 1D-steady-state model, in the vessel wall with a 2D-instationary model. The crust dynamics and a contact resistance between crust and RPV is considered.

![Integral model for the long-term debris-wall interaction (AIDA)](image)

**Figure 3: Modelling approach for the debris-wall interaction in the lower head**

The heat transfer from the upper crust to the water is described by standard correlations (film boiling: Bromley/Berenson; nucleate boiling: Rohsenow, Forster-Zuber, Forster-Greif; natural convection: Fujii-Imura; MFB: Henry, Kalinin, Klimenko-Snytin). The heat exchange with an overlying particulate debris bed is being experimentally and analytically investigated in detail at the Technical University Munich and the IKE of the University of Stuttgart. Therefore, the particle bed heat transfer is estimated in AIDA with a simplified approach using the concept of effective thermal conductivities. Quenching or dryout of the bed is assessed with the particle bed model of Lipinski.

\[^2\] Structure code developed by the Institute for Statics and Dynamics of Air- and Spacecrafts of Stuttgart University with support of GRS [8]
The currently available models for the heat transfer in gaps were not considered adequate for reliable predictions especially in view of counter-current flow situations with small inclinations of the gap. Experimental work in this area is underway at several institutions (e.g. Siemens, FAI, Kurchatov-Institute). Therefore, at the present stage the gap cooling is considered in HEAT2D and PSU in a parametric way by heat sinks.

3 Uncertainty and Sensitivity Analysis

The discussion about validation strategies for severe accident codes gains increasing importance. If these codes shall be employed in the regulation process there will be the need to demonstrate an adequate degree of validation of the models and to understand sufficiently the uncertainties and their impact on safety relevant code results.

The presented debris bed model, for instance, depends on various uncertain parameters such as initial and boundary conditions (e.g. melt mass, decay heat, initial melt temperature, ...), different heat transfer correlations, thermal-physical properties, and other model parameters (e.g. contact resistance, crust porosity, ...), as well as the numerical scheme. The present work illustrates a comprehensive propagation of the different sources of uncertainty and it allows to investigate and quantify the sensitivity of safety relevant parameters to these sources.

The uncertainty and sensitivity analyses were performed with the GRS software system SUSA which supports the probabilistic modelling of parameter uncertainties. SUSA offers Simple Random Sampling and Latin Hypercube Sampling, derives distribution-free quantile estimates as well as statistical tolerance limits and performs statistical tests of distribution hypotheses and fitting of distributions to data. A choice of over 15 sensitivity measures is available for single- as well as continuous-value model output [7, 8].

Two scenarios were taken as an example. The first one is the TMI-2 situation, the second case is a high Ra'-number scenario with a „dry“ melt-vessel interaction and an external cooling of the RPV. For both cases a simple random sample of size 100 was drawn from a joint subjective probability distribution. The number of required code runs does not depend on the number of parameters but on the tolerance limits of the calculations and the confidence level of these limits. The AIDA code was run for each of the 100 parameter vectors yielding a subjective probability distribution for each of the reference output values. These distributions quantitatively express the logically resulting state of knowledge of the considered problem.

3.1 Application to the TMI-2 Accident

Main characteristics of the TMI-2 accident were the high pressure in the RCS and the continuous availability of water in the lower plenum during the accident. The melt which drained into the lower head was principally oxidic. Its mass was relatively small leading to a shallow melt pool and a Ra' number range of $10^{12}$-$10^{13}$.

The quantities needed for these post-accident calculations are basically deterministic, but they are subject to a subjective probability distribution due to the insufficient state of knowledge. The analyses are carried out in two steps. The first step focuses on the quasi-stationary heat-up of the vessel without gap cooling. For that a

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3 The ROAAM methodology provides a structured approach in this respect [9]
preliminary list of 27 important uncertain parameters as well as their ranges was specified (Tab. 1) making use of the TMI-2 VIP results.

They include 9 initial and boundary conditions, 10 thermal-physical properties, 2 parameters for the heat transfer correlations in the melt pool, 1 parameter for the peaking of the downwards heat flux in the pool, 1 parameter for the numerical solution algorithm (minimum accuracy criterion), and 4 other model parameters. The specification of the mixture properties was considered to be more transparent than a variation of the corium composition because of the uncertainties in the methods used to derive the mixture properties. The upper and minimum lower crust thickness, the mean heat flux from the crust to the wall and the maximum mean temperature in the wall were taken as reference output values.

The second step of these analyses aims at the influence of the gap cooling mechanism. 7 additional parameters describing the hot spot, the heat sinks and the time of initiation were defined for this case. The results are not yet available.

<table>
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<th>Parameter</th>
<th>Min/ Max</th>
<th>Ref.-value</th>
<th>Distr.</th>
<th>Parameter</th>
<th>Min/ Max</th>
<th>Ref.-value</th>
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Table 1: List of potentially important uncertain parameters for the TMI-2 scenario
3.2 Application to a high Ra'-number scenario with dry melt-vessel interaction

Due to the specific sequence of the TMI-2 accident a second more prototypic scenario leading to a high Ra'-range has been investigated. The initial conditions of this a priori calculation are principally stochastic but they are interpreted as deterministic quantities with respective subjective probability distributions. They cover, of course, a broader range than in the previous scenario and it has to be observed that they are not strictly independent. The initial and boundary conditions as well as the configuration in the lower head are orientated to an AP600-like situation, i.e. an almost completely filled oxide pool (Ra' = 10^{15} - 10^{16}) which is not covered by water. The pressure vessel is cooled by external flooding. The metallic layer on top of the oxide pool was not considered in this analysis.

A list of 23 uncertain parameters has been defined for this case similar to Table 1. A major problem is still the composition of the core melt, in particular the matter to what degree the melt could contain metallic components. The approach here to capturing this effect was to specify three different corium compositions, of which two are of purely oxidic nature (92%UO_{2}/8%ZrO_{2} and 78%UO_{2}/22%ZrO_{2}) with a probability of 40% each. The third composition (probability 20%) has a small metallic share (80%UO_{2}/12%ZrO_{2}/8%Zr) leading to a solidus-liquidus range of about 600 K. The uncertainties in the thermal-physical properties have been considered by shifting factors.

4 Preliminary Results

4.1 TMI-2 scenario

Figure 4 shows the scattering of the time histories of the average heat flux from the lower crust to the wall over the considered period of time (~ 60 min after melt relocation). Figure 5 the two-sided (95%,95%) tolerance limits of the minimum lower crust thickness, i.e. at least 95% of the joint effects of the considered uncertainties are between the two curves on a confidence level of 95%. As an example, Figure 6 shows the ordinary product-moment correlation coefficients of the minimum lower crust thickness as sensitivity measure for the parameters 11 to 20. A positive sign means that the trend of variations of the input parameter conforms with the result and vice versa.

Some general trends could be observed in the analyses. The uncertainty range of the heat flux from the melt to the crust is fairly small, the crust thickness', however, exhibit a strong variation (Fig. 5). The htc correlation of the lower boundary of the melt pool was the main contributor to the uncertainties of the lower heat fluxes, crust thickness' and wall temperatures. The thermal conductivity of the resolidified corium turned out to be the second main contributor to the uncertainties of the upper and lower crust thickness' (Fig. 6).

The uncertainties of the other thermophysical properties had no substantial influence. The initial melt temperature, the interface temperature between the solid and liquid corium phases and the contact resistance crust-wall had a significant short-term effect. The change of the heat transfer conditions at the upper boundary (time for particle bed formation) did not have an essential effect on the lower boundary, but is important for the uncertainties of the upper crust thickness.
4.2 High Ra'-number scenario with dry melt-vessel interaction

The heat flux from the crust to the vessel necessarily exhibits a stronger variation than in the previous case (about 200-400 kW/m² after 1000s). The two-sided tolerance limits of the minimum lower crust thickness and of a residual wall thickness (part of the wall where the temperature is below 900K) show a large variation (Fig. 7 and 8). The crust thickness’ is altogether very small. In some cases (with metallic corium composition) no crust could form (lower limit in Fig 7).

Figure 4: TMI-2 scenario: Time histories of the average heat flux from the lower crust to the vessel (Runs 1 to 100)

Figure 5: TMI-2 scenario: Two-sided tolerance limits for the minimum lower crust thickness (sample size = 96, β = .95, γ = .95)

Figure 6: TMI-2 scenario: Ordinary product moment correlation as sensitivity measure for the minimum lower crust thickness (parameters 11-20)
The corium composition and the contact resistance between crust and wall turned out to be main contributors to the uncertainties. The influence of the contact resistance could be put down to the weak thickness and thermal resistance of the crust. The initial superheat of the melt showed a significant short-term effect, the upper htc correlation had surprisingly a strong impact in the long term on the lower heat flux and crust thickness (the applied correlations differ considerably in this Ra-number range). The thermal-physical properties did not show any substantial influence.

![Figure 7: High Ra-number scenario: Two-sided tolerance limits for the minimum lower crust thickness (sample size = 100, β=.95, γ=.95)](image)

![Figure 8: High Ra-number scenario: Two-sided tolerance limits for the minimum residual wall thickness (sample size = 100, β=.95, γ=.95)](image)

5 Conclusions

A simplified simulation of the thermal behaviour of the TMI-2 vessel in the course of the accident demonstrated the potential of a gap cooling mechanism and confirmed the VIP estimations as well as other analyses [9].

A refined integral model for the heat transfer between relocated core debris and the pressure vessel was applied to two reference scenarios for the quasi-stationary heat-up of the RPV. These scenarios were analysed in an exemplary way by a fully probabilistic approach considering the various modelling uncertainties. The combined influence of these uncertainties was quantified by derived uncertainty statements. The sensitivity measures indicated where the state of knowledge has to be improved if the uncertainties of these simulation models should be reduced most effectively. It has to
be stressed that the conclusions of these analyses are first of all restricted to the respective model assumptions and the considered scenarios. Nevertheless, some more generic conclusions can be drawn.

Regarding the low Ra'-number scenario with a purely oxidic melt and continuous presence of water in the lower plenum (TMI-2) it emerged that the uncertainties of the code predictions are comparatively small except for the crust thickness. This large variation is mainly due to the heat transfer coefficient for the lower hemispherical boundary of the melt pool and the uncertainties of the thermal conductivity of the resolidified corium. The influence of the other thermal-physical properties was not significant. The impact of the heat transfer coefficient is in all likelihood a result of the differing correlations for partially filled pools. The change of the heat transfer conditions at the upper crust, i.e. the formation of an overlying particulate debris bed (without dryout!), did not have an essential effect on the thermal behaviour of the RPV.

In the high Ra'-number scenario with a dry melt-vessel interaction and an external cooling of the RPV it was in addition assumed that a more metallic melt with a content of unoxidised Zirconium is possible. The code predictions necessarily exhibited a stronger variation, but they are within reasonable bounds. It turned out that the crust thickness is generally small. In some cases with the metallic corium a crust could not be formed in the regions of higher heat fluxes. The corium composition and the contact resistance between crust and vessel were the main contributors to the uncertainties. The latter points to a modelling weakness. The possibility of core melts with a large range between solidus and liquidus has to be further investigated.

In view of the gap cooling the large variations of the lower and upper crust thickness' indicate that the matter of crust stability is of prime importance for the formation of the gap. It can be argued that the verification of the gap cooling as an inherent mechanism appears very difficult.

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Simulation of the arrival and evolution of debris in a PWR lower head with the SFD ICARE2 code

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Abstract

In a severe accident scenario, the prediction of vessel failure is related to the prediction of the behaviour of solid and liquid debris which have fallen into the lower plenum. One of the difficulties is to define the initial debris bed conditions. They will depend on the presence of water or not in the lower plenum. They will also depend on the rate and composition of the falling debris. In this context, IPSN performs calculations using the ICARE2 code in order to predict the core degradation in a scenario similar to TMI-2, and to estimate the so-called initial conditions of the debris bed, i.e. the history of the debris falling into the lower plenum and the evolution of this debris bed.

The latest version of ICARE2 (V3mod0) which deals with molten pools and debris bed allows to follow the materials from their early melting in the core region to their later relocation into the lower plenum.

A description of the modeling of the debris bed and molten pool formation is provided in this paper. The debris bed modeling is based on a porous medium approach. Mass and energy conservation equations for each of the three phases and momentum conservation equations for the liquid and gas phases are solved. Up to now, due to the lack of knowledge, no model has been developed to estimate the debris size distribution. It is chosen by the user at the beginning of the calculation. As the temperature increases in the debris bed, a molten pool appears and starts to grow. Thermal effects of the natural convection movements in the pool are taken into account using classical correlations.

The present study corresponds to a first application of the new capabilities of ICARE2. However it shows the interest of such an approach for a problem where the behaviour in the vessel is closely related to the previous events that occurred in the core region. Improvements are foreseen, especially for the natural convection, crust formation and interaction with water.
Introduction

In most of the accident scenarios considered up to now, the behaviour of debris in the lower plenum of a PWR and the prediction of their coolability are related to the history of debris arrival into the lower plenum. The mass and composition of the debris falling into the lower plenum may vary, according to the accident scenario. The radiative heat transfer from the lower debris to the core structure can lead to an additional collapse of metallic debris into the lower plenum.

The need to quantify such phenomena points out the interest of predicting simultaneously the behaviour of materials in the core region and in the lower plenum. In that respect, the ICARE2 code, developed by IPSN, has been designed to simulate debris formation inside the core, as well as later relocation down to the lower plenum, and heat and mass transfers between those two regions. From the core to the lower plenum, the coupling comes essentially from the collapse of debris (solid or liquid). The coupling from the lower plenum to the core region is either a radiative exchange when there is no water, or a steam flow when there is water in the lower plenum. In order to simulate all these phenomena, models have been developed to calculate debris behaviour, with a special emphasis on melt relocation and radiative exchanges through large cavities. The presence of liquid water will be taken into account in a future version of the code.

One of the advantages of our approach is the use of the same models for the thermal behaviour and relocation of the debris in all parts of the vessel. These models are briefly described in the paper. A PWR accident scenario is simulated and the most significant results are presented. These results have been obtained with a version of ICARE2 which is still under development. Thus they should be considered as a first application of the new capabilities of the code.

Physical modeling

ICARE2 describes a set of components representing either the vessel structures (fuel and control rods, barrel, vessel, etc.) or the solid debris, or the melt, or the gas. The vessel volume is discretized on a two-dimensional cylindrical meshing. Each component is described by its volume fraction in the mesh, its composition and its temperature. The solid components also have a specific geometry: rods are represented as cylinders, debris particles as a set of spheres, etc... The physical analysis of late phase degradation is based on a porous medium description. The previous work of Dosanjh and coworkers ([1], [2]) has proved the ability of such models to describe melt propagation and relocation and Mayr et al. [3] have successfully used a porous medium approach to model the dry-out of a debris bed. After the strong oxidation of the claddings, some parts have been removed by melting and it is likely that the fuel rods cannot keep a cylindrical geometry. The resulting geometry of the fuel pellets is assumed to form a porous medium, usually called “debris bed”. The solid particles can collapse, according to some user criteria. In the present modeling, a full set of conservation equations is used. This includes mass conservation for each phase, momentum conservation for the liquid and gas phases, and energy conservation for each phase. The assumption of thermal equilibrium between the three phases is not made here. The momentum equation for the gas is either one-dimensional (separate channels) or two-dimensional. It is not coupled to the liquid momentum equation but this last assumption is not really restrictive. The variables chosen to describe the volume fraction of each phase are the porosity $\epsilon$ and the liquid saturation $S_l$. All the details about the modeling can be found in the reference manual of ICARE2 code [4].
Material relocation

The relocation of molten material is calculated from momentum conservation equations for the liquid phase, except in the molten pool region, where natural convection flow is not modelled. All the materials are supposed to move at the same velocity. The possible stratification or relative diffusion of species in the corium are not modelled. The relocation is treated according to two different processes: either the flow of liquid mixture along the rods (one-dimensional) or the flow through a porous debris bed (two-dimensional). For the liquid flow, the classical Darcy’s law (see for example Scheidegger [5]) is used. The mass and momentum conservation equations can be written as:

\[
\frac{\partial (\epsilon S_l \rho_l)}{\partial t} + \nabla (\rho_l \mathbf{u}_l) = \dot{m}_{fus} \tag{1}
\]

\[
\frac{\mu_l}{K_{K_l}} \mathbf{u}_l + \nabla p_l - \rho_l g = 0 \tag{2}
\]

where \(\dot{m}_{fus}\) is the mass rate of fusion of the solid debris. In the debris bed regions, the liquid pressure is deduced from the gas pressure with the capillary pressure relation. In the regions where the porosity is very close to one (no porous medium effect), the liquid pressure is calculated differently: the liquid is assumed to be static, and a simple hydrostatic pressure is calculated.

Thermal behaviour

The liquid and solid phases thermal behaviour in the debris bed are governed by two energy conservation equations (one for each phase), as can be found for example in Kaviany [6]. For the solid phase:

\[
\frac{\partial ((1-\epsilon) \rho_s H_s)}{\partial t} + \nabla \cdot (\rho_s \mathbf{u}_s H_s) = \nabla \cdot (\lambda_s \mathbf{e}_{eff} \nabla T_s) + h_{sl} A_{sl} (T_s - T_l) + h_{sg} A_{sg} (T_s - T_g) + (1-\epsilon) \rho_s q_s + Q_{chem} \tag{3}
\]

For the liquid phase:

\[
\frac{\partial (\epsilon_s S_l \rho_l H_l)}{\partial t} + \nabla \cdot (\rho_l \mathbf{u}_l H_l) = \nabla \cdot (\lambda_l \mathbf{e}_{eff} \nabla T_l) - h_{sl} A_{sl} (T_s - T_l) + \epsilon_s \rho_l q_l \tag{4}
\]

where \(H_s\) and \(H_l\) are respectively the mass enthalpy of the solid and liquid phases and \(\lambda_s\) and \(\lambda_l\) are the effective thermal conductivities; \(h_{sl}\) is the interstitial convection heat transfer coefficient, and \(A_{sl}\) is the contact area of the solid matrix with the liquid; \(q_s\) and \(q_l\) are respectively the volumetric heat release rates in the solid and liquid phases. Dispersion effects are included in the effective conductivities. The assumption of thermal equilibrium between liquid and solid phases is not made, which makes this model very general, and allows to deal with any solid-liquid configuration. An oxidation model of \(Z\) solid particles may be activated by the user. In this case, a chemical reaction heat source term \(Q_{chem}\) is added to the solid phase equation. Diffusion terms include the effects of conduction in the solid, liquid and gas phases, as well as radiation between the solid particles. They also include dispersion effects. The overall heat flux is usually expressed as a classical diffusion flux, with an effective diffusivity coefficient \(\mathbf{e}_{eff}\). Among the numerous correlations established for this diffusivity, the relation from Imura-Yagi [7] was used in the present calculation. The radiative effects were included by the addition of the Luikov model [8]. When cavities (empty regions) form in the computation domain, a procedure has been developed to identify the surfaces bounding the cavities and to calculate the radiative heat transfer between them. This procedure estimates the view factors between
elementary surfaces of the cavity and the total exchanges area between surfaces. It is called at each time step, because the cavity geometry changes. This procedure is not restricted to a single cavity but is also able to calculate the heat transfers in a set of cavities, for examples in the core and in the lower head.

**Molten pool**

As the solid debris melt and molten materials move through the bed, regions of pure liquid magma may appear (for example above the lower crust). A similar configuration can also be found if the magma happens to fall into the lower head of the reactor vessel. When the magma forms such a molten pool, natural convection takes place within the pool. This movement cannot be calculated without considerably increasing the computation time. For this reason, it was chosen not to calculate the 2-dimensional velocity field for the liquid phase in the molten pool. Only the thermal effects of the natural convection movements in the pool are taken into account. This is done according to some classical correlations (for instance Mayinger et al. [9]).

**Crust formation**

The penetration of the liquid magma into cooler regions, or the contact with a cold wall may lead the magma to solidify. This stops the movement of the magma, and a crust forms. A difficulty arises from the fact that a fraction of solidified magma may exist, even though the average magma temperature in the mesh cell is higher than the liquidus temperature. It is necessary to try to estimate the localization of the solidification front inside the mesh. Otherwise, the accuracy on the position of this front depends on the meshing (size and discretization), and the resulting heat fluxes at the boundaries of this mesh cell are miscalculated. This problem becomes especially critical when a hot corium is in contact with a cold structure (for example the lower head, which can be cooled externally). In this case, a very sharp temperature gradient between the wall and the corium exists, and a crust may form on the wall. The thickness of this crust is generally lower than the size of the mesh, and a model is required to estimate the crust characteristics, which cannot be found directly from the local magma properties. A similar difficulty appears when one tries to estimate the radiative heat flux from the top of the molten pool. It is necessary to know the surface temperature, which can be significantly lower than the pool temperature. For both cases, a model allows to modify the heat flux by calculating the temperature at the surface of the pool.

**Results**

**Meshing and boundary conditions**

The core region is divided into 6 rings (1-D channels), the by-pass is represented by one ring, and the downcomer by one ring. There is a total of 43 axial meshes, 30 being located in the core region. The size of the rings decreases from 0.81 m for the inner one to 0.13 m for the external one. The maximum time step is 50 s. The calculation starts at the beginning of the core uncovering. It is assumed that all the core region is quickly uncovered. A residual steam flow is prescribed in input data, as well as the power evolution.
Debris formation and relocation to the lower plenum

Rods temperature increases rapidly due to residual power. Heat is removed from the rods either by convection with the steam flow or by radiation with the neighbouring rods. Because of the low values of the view factors between rod rings, the temperature in the center of the core becomes much higher than in the external part. The axial temperature profile is a result of the power distribution, but the heat transport by the steam flow shifts the high temperature region towards the top of the core. When the temperature exceeds 1000 K, zircaloy oxidation becomes significant, with an escalation after 1850 K. The energy supplied by this reaction is very important and accelerates the core heat-up. Oxidation of the claddings produces also a large hydrogen release. For high cladding temperatures, molten zircaloy is present between the fuel pellets and the external zirconia (oxide) layer. If the zirconia thickness is smaller than 300 μm, cladding rupture occurs and the liquid mixture starts flowing down (candling). If there is no rupture, the liquid zircaloy is held in place and dissoves the fuel pellets. When the mixture flowing along the rods reaches the water, it freezes rapidly. The accumulation of frozen material forms a crust around the rods, above the water level. When a channel is blocked because of this crust, the vapor flow is set to zero in the corresponding channel and distributed to the other open channels. When claddings disappear, fuel pellets are transformed into debris. The resulting debris bed, and its evolution can be seen on figure 1, where the fuel rods are replaced by a uniform cell with a color representing the amount of material in the mesh. One can observe the accumulation of materials in the bottom part of the core, at 4000 s. This corresponds to a mixture of solid and liquid debris, above a crust which has formed in the cold part of the core. Due to the initial power profile, the power released in the bottom part is rather low. For this reason, the liquid front moves toward the external part of the core, rather than downwards, and eventually reaches the external baffle, which melts. At this point, the liquid mixture can flow down to the lower plenum, through the by-pass. This event occurs around 4500 s in our simulation. After that, the debris relocate continuously toward the lower plenum, as can be seen on figures 1 and 3. Around 6500s, the center of the lower plate melts, as a result of conductive heating from the debris located above and radiative heating from the debris in the lower plenum. This causes the remaining liquid in the core region to be drained into the lower plenum. One can notice that there are some debris and rod remnants above the melted plate. This will be improved in a future version where rod remnants will be allowed to collapse. The composition of the debris in the lower plenum (represented on figure 4) changes during the accident sequence. After the first relocation, there is about 65% of UO₂ and 5% steel, 20% Zr and 10% ZrO₂. Later, the melting of large steel structures such as the baffle, barrel and lower plate increases significantly the amount of steel in the lower plenum, up to 25%. The fraction of UO₂ decreases to 40% whereas the other materials keep the same relative fraction. At the end of the calculation, there is the same amount of debris in the core region and in the lower plenum (fig. 3). Another interesting result is the evolution of the debris temperature in the lower plenum. This can be seen on figure 2. After the first relocation, the debris arrive at a temperature around 2200 K. At 8000 s, the maximum temperature reaches 2700 K in the center of the pool, whereas the top and the external parts are colder.

Conclusion

Models have been developed to simulate the thermal and mechanical evolution in a PWR vessel during the late degradation phase of a severe accident. It is assumed that severely degraded core materials can be represented as a mixture of solid particles and liquid corium,
forming a partially saturated porous medium. The configuration can range from a dry debris bed to a molten pool. Most of the models are based on porous media studies or existing models for molten pool heat transfers. Particular attention was paid to the formation of debris after cladding oxidation, the relocation of debris, and the radiative heat exchanges through large cavities.

A numerical simulation of a PWR accident scenario was presented. Obviously, the modeling is still incomplete, especially in the lower plenum, where the presence of liquid water is not taken into account. Some improvements are also necessary for the calculation of liquid and solid debris relocation in specific configurations (by-pass, grid). However, the present modelling shows that it is possible to predict the evolution of the mass, temperature and composition of debris arriving into the lower plenum. The continuous relocation of debris, and the radiative heat exchanges between the lower plenum debris and the lower plate indicates a comprehensive modeling such as the one presented in this paper may be necessary for some accident scenarios.

As a general conclusion, when dealing with in-vessel debris retention and coolability, one should bear in mind that debris are not entirely located in the lower plenum. Our calculation shows that a significant part of the debris can remain in the core region, even after a massive relocation into the lower plenum. Such knowledge is important for accident management. In case of a late water injection for instance, the pressure increase will depend on the temperature of materials in all parts of the vessel. The ICARE2 approach allows to predict the evolution of debris everywhere in the vessel. For example, it is possible to predict the history of the arrival of debris into the lower plenum (mass, temperature, composition, as a function of time).

At present, many physical phenomena are not modelled yet. And it is likely that limiting assumptions will be made in some models. Hence, the use of dedicated codes will still be necessary, for detailed studies, such as the quenching of the debris or the accurate prediction of vessel deformations or complex 3-D phenomena. But the approach chosen in ICARE2 is very useful for predicting the repartition of materials inside the vessel and their thermal behaviour under different boundary conditions.

References


Fig. 1: Evolution of the material distribution in the core.
Fig. 2: Evolution of the temperature field in the core.
Fig. 3: Mass of debris in the core and lower plenum
ICARE2 V3Mod0

Fig. 4: Lower Plenum Debris Composition
ICARE2 V3Mod0
Material behaviour and interactions have consequences on In-Vessel Retention capabilities. The paper will focus on some important aspects.

1) Behaviour of oxidic corium mixtures at elevated temperatures

Depending on its composition, oxidic corium (UOZr) may undergo separation at elevated temperatures. This separation may be due to, at least, two effects:
- existence of a miscibility gap above the liquidus temperature of the mixture: this gap has been studied in the ISABEL experiments,
- separation due to density effects in the liquid-solid mixture: the results of the RASPLAV experiments are analyzed and the phases formed have been recalculated with the Gemini2 software and its database on corium.

The second effect tends to increase the thickness of the metallic layer which accumulates above an oxidic pool (U,Zr)O2. This is deemed to decrease the so-called focusing effect, while the first effect, because the density of the metal rich phases is higher than the oxide rich one, results in a reverse segregation (metallic layer due to the miscibility gap below the oxidic one) and may not affect this "focusing effect".

1-a) miscibility gap and density effects

A miscibility gap in the liquid state is found in the O-U system [Edwards et al., 1966]. The purpose of the present study was to determine the extent of the liquid miscibility gap in the O-U-Zr ternary system in order to improve the O-U-Zr phase diagram description and to predict the possible segregation of the liquid phases at high temperatures in the corium.

(O,U) and (O,U,Zr) model corium are melted by electron bombardment and then quenched in the ISABEL facility. In case of a composition entering the miscibility gap, the microstructure of the quenched ingot is constituted of spherical droplets corresponding to a minor oxidic liquid in a matrix made of the major metallic liquid (Figure 1). The overall chemical analysis of both droplets and matrix allows to determine overall compositions of both liquids. By this method, two tie-lines are experimentally determined in both O-U at 3090 K and O-U-Zr at 3223 K systems (the latter is drawn in Figure 2). The present results show that the miscibility gap has a large extent in both O-U and O-U-Zr systems. The high concentration of droplets located near the ingot surface indicate that the minor oxidic liquid phase may segregate at the ingot surface by a gravity-dependent effect.

Figure 1: Droplets of the oxide rich liquid (L2) in a matrix of metallic liquid (L1)

Figure 2: O-U-Zr isothermal section calculated at 3223 K where the experimental tie-line is reported
The present experimental results as well as data from the literature are used to calculate the O-U-Zr phase diagram with the Thermo-Calc software. It allows to eliminate the inconsistency concerning the oxygen solubility limit in liquid uranium in the O-U system. Our experimental results on the O-U miscibility gap can only be compatible with the low solubility values of Edwards [Edwards et al., 1966]. Furthermore, our experimental data on the O-U-Zr miscibility gap in the liquid state are consistent with the α-Zr(O)-UO₂ and Zr-UO₂ sections reported respectively by Politis [Politis, 1975] and Juenke [Juenke et al., 1969]. On the other hand, the recent experimental work of Hayward [Hayward et al., 1996] does not predict any miscibility gap in the liquid state along the α-Zr(O)-UO₂ diagram and has to be analysed. The phase diagram calculation allows to extrapolate the miscibility gap versus composition and temperature in the O-U-Zr system.

The present miscibility gap description is used to estimate the density of the two liquid phases. A very simple model considering an ideal mixture of the U, Zr, UO₂ and ZrO₂ pure constituents is used. For Zr contents below 35 at.%, the oxide liquid L2 is the lightest one. The miscibility gap extent decreases with both temperature and zirconium content in the (O,U,Zr) corium. The density difference is highest in the O-U system and will be less important by increasing both temperature and Zr content. The present result is consistent with the experimental study which shows that the segregation is the most important in the O-U system, leading to the formation of an oxide liquid layer at the ingot surface.

1-b) Separation due to density effects in the solid-liquid mixture: analysis of the Rasplav experiments results

The Rasplav AW200-1 test was performed in 1996: C-22 corium (metallic corium containing non oxidized Zr) was melted in the Rasplav facility.

During the test, temperatures about 200°C higher than the liquidus temperature of C-22 were measured in the middle part of the pool, and could not be explained by superheating of the melt because of the very low extracted heat fluxes. After cooling and opening the facility, separation of the melt in two layers was observed. One layer Zr-rich (called "metallic" one) was located in the upper part of the device (20 vol.%), and the other one, UO₂ and ZrO₂-rich was located in the lower part (80 vol.%).

These results may not be explained by separation due to the miscibility gap of the ternary system U-O-Zr, for the following reasons:
- first, due to density effects (see above), the metallic layer would have segregated in the lower part of the facility as the density of the "metallic" liquid L2 is expected to be higher than the density of the oxidic liquid L1.
- Second, the measured temperature would have been the same in the two layers and the difference of 200°C would not be explained.
- Third, the average composition C-22 is just at the limit of the miscibility gap, even for a large extent of the miscibility gap modelling: thus, the fraction of each layer (which is given by the lever rule on the corresponding tie-line) would not fit the measured fractions.

Another interpretation was proposed, involving a segregation of the two layers during C-22 melting. The melting or solidification range of such compounds is very large (the difference between the solidus and liquidus temperatures can be of more than 1000°C, see fig.3)

![Figure 3: equilibrium phases in the O U Zr system](image)

From these curves it can be observed that a "metallic" liquid (mainly composed of αZr(O)) co-exists with the oxidic solid (U, Zr)O₂ over a large temperature range (from 2200 to 2700K at least) for a liquid fraction around 20 at.%.  

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It is postulated that the liquid may gather through the solid microstructure, just before the liquidus temperature of the mixture is reached. At the same time, because of its lower density (the liquid contains mainly Zr and O and the solid is mainly constituted of UO2), the liquid is supposed to separate, and to go to the upper part of the pool. The reason for this separation is not understood.

The liquidus temperature of the solid phase, isolated from the liquid one which has separated, may thus reach the liquidus temperature of the residual oxide (U,Zr)O2. This temperature, calculated with GEMINI2, is about 2970 K, which would then be the solid-liquid boundary temperature for the lower pool according to our model approach. This corresponds to the elevated temperature measured in the lower part of the molten pool.

Assuming this scenario, it is also possible to calculate the equilibrium state after solidification, considering each phase separated from the other. The calculated final compositions of the phases agree with the experimental results.

If such a separation of the metallic part of the corium could occur in a reactor vessel, that would decrease strongly the possibility of focusing effect: it is thus important to understand this separation mechanism and to evaluate if it could happen in reactor conditions.

2) Interaction between metallic layer and vessel wall

Two different items are examined in this part. The first one is a theoretical approach based on thermodynamical equilibrium calculations, the aim of which is to evaluate the lower interface temperature between the metallic pool and the vessel wall due to physico-chemical dissolution to check whether the mechanical behaviour of the cold part of the wall is or is not modified by this interaction. The second one deals with the possible migration of low melting point metallic elements (chromium, tin, indium...) in the solid steel wall that may reduce its mechanical properties too: a test is performed in the ISABEL facility (CEA Saclay) in order to analyse the possibility of such a migration under real thermal and stress conditions.

2-a) Physico-chemical interaction of a corium with the carbon steel vessel wall

All the following calculations were performed under equilibrium hypothesis with GeminII2 software and the TDBCR associated data base on corium.

An in-vessel retention scenario assumes an external cooling of the vessel wall; the external surface temperature may be close to 150°C. The question is to determine at which temperature the metallic pool might dissolve the steel wall.

We took, as an example, a 900MW PWR core inventory in which only 50% of the total zirconium is supposed to be oxidized, giving a metallic layer composition close to the following one:

\[ \text{Zr : 8000 kg, Sn : 300 kg, Cr : 5 000 kg, Ni : 3 000 kg, Fe : 18 000 kg} \]

Due to the physico-chemical equilibrium hypothesis between the two layers (metallic and oxidic ones), U is present in the metallic part with a maximum amount calculated to be about 20wt% at 1500°C; this value being related to the metallic Zr initially present in the metallic layer.

The next step is to estimate the temperature at which may locally co-exist a metallic liquid of such a composition and a solid wall, and to determine their final compositions.

Thermodynamical calculations showed that, for interface temperatures higher than 1100°C, there is no equilibrium state between the above metallic liquid and the solid wall: the iron saturation of this liquid layer cannot be reached. Below this temperature, the liquid, dissolving some amount of iron in order to reach its solubility limit, may be locally in equilibrium with the remaining steel wall (which thickness has thus decreased).
As the temperature gradient in the vessel wall is imposed by the external cooling, the knowledge of the interface equilibrium temperature allows us to estimate the remaining thickness of the vessel (see the following sketch).

As the mechanical resistance of the vessel is mainly controlled by the external cold part (below 600°C), it is concluded that the mechanical behaviour of the vessel is not affected by physico-chemical dissolution of the steel wall by the metallic corium, even for a metallic corium containing up to 20 wt% uranium.

2-b) Migration of low melting point metallic elements in the solid vessel wall.

Tin, which is present in the metallic corium, may migrate through the solid vessel. According to the literature [Barbier et al., 1997], it is reported that additions of tin to high strength steels (e.g., AISI 4140 steel [Musek et al., 1982]) can reduce the ductility and fracture strength for tensile specimens tested at elevated temperatures. In order to assess the risk of embrittlement of 16MND5 steel by tin, a specific experiment has been realized to study the possibility of diffusion of this element into the steel, under real thermal and stress gradients.

The test was performed in the ISABEL facility. The vessel was simulated by a small crucible externally cooled containing a metallic simulated corium mixture (without U) including tin. The heat flux delivered by the melt to the wall was about 1 MW/m² and it has been maintained for 1 hour. At the end of the experiment, a layer of 3 mm was molten from the inside of the test wall. Then, samples were taken in the corium/steel interaction area for examination. They were characterized by scanning electron microscopy (SEM) and chemical analysis was made by energy dispersive spectroscopy (EDS).

At the end of the experiment, the average composition of the metallic corium was found equal to (wt. %): 65.5 Fe - 8.5 Cr - 8.6 Ni - 14.7 Zr - 2.7 Sn. In the vicinity of the steel, the corium exhibited a complex microstructure as shown in Fig. 4. It is composed of (Fe,Cr)-rich phases (dark areas) embedded in an eutectic structure. The white areas of this structure are Zr-rich (46.5 Fe - 33.6 Zr - 10.2 Ni - 3.4 Cr - 6.3 Sn in wt.%) and they are smaller far from the corium/steel interface. The composition of the dark areas is (wt. %): 82.2 Fe - 12.1 Cr - 5.1 Ni - 0.3 Zr - 0.3 Sn.

Figure 4: Microstructure of the corium/steel interaction
Concentration profiles have been measured in the steel, beyond the interface (Fig. 5). It can be seen that the Cr and Ni contents are decreasing while the Mn content is increasing (on about 40 μm) before the initial concentrations of the 16MND5 steel are found (i.e., 0.2 Cr - 0.9 Ni). It has to be noted that the Cr and Ni concentrations in this transition zone (= 12 and 5 wt. %, respectively) are very similar to those analyzed in the dark phases of the corium area. It seems that this diffusion zone is detached with time and then dispersed in the metallic corium. On the contrary, no significant change in the profiles is observed for the Sn and Zr elements, which have low concentrations (less than 0.2 and 0.08 wt. %, respectively).

Thus, taking into account the accuracy of the apparatus, no volume diffusion of tin has been detected into the steel. However, the analysis cannot show if tin could diffuse along the grain boundaries (this point is under analysis). Nevertheless, referring to these preliminary results, it seems unlikely that the mechanical properties of the steel be significantly decreased beyond this transition zone.

3) A model for the calculation of melt viscosity

Whatever the scenario considered, from the beginning of core melting up to the control of the accident by stopping and cooling the corium, the knowledge of corium physical properties versus temperature is essential to predict scenario evolution with a view to managing the accident. The list of physical properties involved in all the phenomena which could occur is impressive; however only some of these properties are of significant importance. Among these, viscosity (in fact, corium rheological behaviour) plays a major role in many phenomena such as core-melt down and corium discharge from reactor pressure vessels.

For these reasons, it is important to be able to predict the rheological behaviour of corium of different compositions (essentially based on UO₂, ZrO₂, Fe₃O₅, and Fe for in-vessel scenarios) at temperatures above the liquidus temperature or between the solidus and the liquidus temperature.

3-a) Liquid phase viscosity

One of the first theories concerning the viscosity of liquids is due to Andrade (1934a). This theoretical modelling is mainly based on the hypothesis that, at the melting point, a liquid has a structure comparable to its solid phase. He proposed the following expression of the liquid viscosity at the melting point:

\[ \mu_T = K \left( \frac{A \cdot T_m}{V_A} \right)^{\frac{1}{2}} \]

in which, \( \mu_T \) (Pa.s) is the viscosity at the melting temperature (T_m (K)), K a constant, A (kg) the atomic weight and V_A (m³) the atomic volume.
When temperature is above the liquidus temperature, Andrade (1934b) assessed that only molecules having an energy higher than the activation energy $Q_n$ can exchange momentum; thus he extended his formula and proposed the following relationship:

$$\mu_T = K \left( \frac{A \cdot T_m}{V_A \cdot T_m} \right)^{\frac{1}{2}} \exp \left[ \frac{Q_n}{R \left( \frac{1}{T} - \frac{1}{T_m} \right)} \right]$$

in which $\mu_T$ is the viscosity at temperature $T$ and $R$ the ideal gas constant.

The use of the Andrade formula to predict liquid oxide viscosities is often limited by the fact that the molecular weight and volume of an oxidic mixture at melting temperature are generally unknown.

Therefore, we propose (Sudreau, Cognet 1997) to use the following formula:

$$A = \sum_i x_i A_i$$

where $x_i$ is the atomic fraction of the $i$ component and $A_i$ its atomic weight;

$$V_A = \sum_i x_i V_i = \sum_i x_i \frac{A_i}{\rho_i}$$

in which $V_i$ the atomic volume of the $i$ component and $\rho_i$ its density at the melting point.

The determination of $K$ and $Q_n$ is based on experimental correlations; for $K$, Andrade, himself, proposed the following value which seems to be the best one:

$$K = 0.161 \times 10^{-6} \text{ kg}^{1/2}\cdot\text{m} \cdot \text{K}^{-1/2}\cdot\text{s}^{-1}.$$  

$Q_n$ can be assessed by using the following formula proposed by Grosse in 1963:

$$Q_n = 1.8 \cdot T_m^{1.345} \text{ (J. mole}^{-1})$$

3-b) Viscosity in the solidification range

It is supposed here that solid and liquid stay mixed in the freezing interval.

When temperature is within the solidification range (between solidus and liquidus temperatures), due to the appearance and growth of solid particles, the rheological behaviour of the mixture is no longer Newtonian; nevertheless its behaviour can be approached by an apparent viscosity ($\mu_{app}$). In this temperature range, many parameters play a role in the rheological behaviour of hydrodynamic suspensions: liquid phase viscosity ($\mu_l$), solid volume fraction ($f_s$), shear rate, cooling rate, particle shape, ... A lot of empirical and semi-theoretical correlations have been proposed to calculate this apparent viscosity. However, these relationships need always the determination of at least one, often two or three, parameters whose physical meaning is not very clear. Among these correlations, and because of the lack of data on corium in the solidification range, we propose to use the Thomas correlation:

$$\mu_{app} = 1 + 2.5 f_s + 10.05 f_s^2 + 0.00273 \exp (16.6 f_s)$$

which is certainly not better than others but which is based on only one parameter: the solid volume fraction ($f_s$). Though the hypothesis of thermodynamic equilibrium is certainly not correct for the whole mixture, this parameter can be provided by a thermo-chemical computer code such as GEMINI.

Solid and liquid may also separate in a corium pool under quasi-state conditions. The model approach is then very different [K. Froment, J.M. Seiler, 1997] and the viscosity to be considered is the viscosity of the residual liquid phase.

4) Baryum release and residual power: residual power distribution during freezing

4-a) Baryum release and residual power

VERCORS is a semi-analytic experiment dedicated to the study of the fission products (noted FP in the following text) release from irradiated fuel [Andre et al., 1996]
We focussed in this work only on the results in terms of total release amounts for each FP.

Two experiments have been chosen among the database, i.e. VER CORS 4 and 5 [Ducros et al., 1996], in order to compare the predictions of the GEMINI2 software and its database to the measured releases. In these experiments temperature levels of about 2300°C were obtained under two different compositions of the gaseous flow (one is reducing, the other oxidising atmosphere).

The experimental conditions of the two tests (temperature and gas flows) were used to determine the equilibrium conditions of the calculations.

The comparison between measured releases and the calculation results are summarised in the following table.

The calculated results correspond for each element to the ratio of the total FP amount in the gaseous phase at 2300°C reported to the initial FP inventory.

<table>
<thead>
<tr>
<th>weight %</th>
<th>VERCORS 4 (reducing atm.)</th>
<th>VERCORS 5 (oxidising atm.)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Calculated releases</td>
<td>Measured releases</td>
</tr>
<tr>
<td>Ba</td>
<td>66 (55)</td>
<td>66</td>
</tr>
<tr>
<td>La</td>
<td>1 (0.6)</td>
<td>&lt;3</td>
</tr>
<tr>
<td>Ru</td>
<td>7 (4.6)</td>
<td>7</td>
</tr>
<tr>
<td>Sr</td>
<td>21 (10.8)</td>
<td>&lt;6</td>
</tr>
<tr>
<td>U</td>
<td>5 (3.3)</td>
<td>2</td>
</tr>
<tr>
<td>Zr</td>
<td>0 (0)</td>
<td>&lt;3</td>
</tr>
</tbody>
</table>

(*) : taking into account the existence of the UO2-SrO compound in the (UO2,SrO) quasi-binary system.
The values between () are calculated for a reduced O partial pressure.

It can be observed that measured releases are not very different between the two tests and thus do not seem to depend on the composition of the gas (reducing or oxidising) for these experiments and only for these FP (other very different releases are measured on Mo and Rh in the different atmospheres, but these elements, not belonging to the database, are not calculated here). The calculated values are also close to the measured releases except for two elements (Sr and Ru), for which the calculated release is very sensitive to the composition of the atmosphere.

Some parametric studies were undertaken for Ru and Sr releases:

- it is possible to approach the measured releases for Sr assuming the existence of some definite compound (either UO2-SrO in the ternary system, either some other definite compound which could appear in a quaternary system : analysis of recent works are going on),

- the Ru measured release corresponds to a lower oxygen partial pressure then assumed in the previous calculations : the consistency of the database was checked comparing Ru releases for oxidising atmospheres (Hunt et al, 1994 - Iglesias et al., 1990) to calculations and showed a good agreement.

It is generally assumed in the calculation of the residual power dissipated in the corium that baryum is not released. However, the release of Baryum is significant in these experiments and the release of this element has a strong influence on the level of the residual power (about 20% reduced).

4-b) Distribution of the residual power in an oxidic corium

A repartition of fission products in the solid and liquid phases has been calculated during solidification of oxidic corium under assumption of thermodynamic equilibrium with the GEMINI2 software and its database on corium.

An in-vessel corium composition was considered (only seven FP choosen as representatives of families are modelised in the database), with the following inventory:
From the thermodynamical equilibrium calculations which give the elements and phases compositions for each temperature, we evaluated the residual volumetric power distribution, assuming that:

- the mixture density may be estimated applying a simple mixture rule of the elementary species densities (for UO₂,ZrO₂...) which amounts are known from the previous thermodynamical calculations,

- the volumetric power of one phase is due to the elementary contribution of each FP according to its amount

These calculations conclude that there is no significant difference of volumetric power dissipation between solid and liquid phases.

**Conclusion**

This paper presented material behaviour aspects that may have consequences on In-Vessel Retention capabilities:

- The physico-chemical behaviour of the corium may result in a separation two layers, one being oxide rich and the other more metal rich, due to some miscibility gap (like in the UO₂/Zr system) or due to other mechanisms (like in the RASPLAV experiments according to our interpretation). According to the density of the two separated phases, the metallic one can be found in the upper or in the lower part of the pool. If it relocates in the upper part (like in RASPLAV), this will decrease the focusing effect which is an important problem for In-Vessel retention. However, the mechanism leading to separation in RASPLAV is not well understood and the extrapolation to reactor conditions is not possible. Complementary investigations are necessary.

- The mechanical behaviour of the vessel seems not to be affected by physico-chemical processes such as steel dissolution by the metallic liquid corium, tin diffusion (but complementary microprobe examinations have to be performed to be sure that tin did not diffuse in the grain boundaries).

- The viscosities of In-Vessel liquid corium layers are estimated by an extension of the Andrade formula. The effect of a solid fraction mixed with the liquid can be taken into account, however this may not be pertinent to the reactor approach if solid and liquid phases separate.

- The Thermodynamic approach seems to be a good tool for fission products releases estimations: the significant Baryum release, higher than generally assumed, which was measured in the VERCORS experiments and which is predicted by thermodynamical calculations, has a strong influence on the residual power in the liquid corium (about 20% reduced).

The distribution of the volumetric power dissipation between solid and liquid phases is predicted to be quasi-uniform.

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Politis C., Report KFK 2167 (1975)

Sudreau F., Cognet G.: "Corium Viscosity Modelling above Liquidus Temperature" Nuclear Engineering and Design - 1997
EXPERIMENTAL DATA ON HEAT FLUX DISTRIBUTION FROM A VOLUMETRICALLY HEATED POOL WITH FROZEN BOUNDARIES

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ABSTRACT

The COPO II experiments are confirmatory experiments and a continuation project to the earlier COPO I experiments. As in COPO I, a molten corium pool on the lower head of a RPV is simulated by a two-dimensional slice of it in linear scale 1:2. The corium is simulated by water-zincsulfate solution with volumetric Joule heating. The heat flux distribution on the boundaries and the temperature distribution in the pool are measured. The major new feature in COPO II is the cooling arrangement which is based on circulation of liquid nitrogen on the outside of the pool boundaries. The use of liquid nitrogen leads to formation of ice on the inside of boundaries. Two geometrically different versions of the COPO II facility have been constructed: one with a torispherical bottom shape, simulating the RPV of a VVER-440 reactor as COPO I, and another one with semicircular bottom simulating a western PWR such as AP600. The modified Rayleigh number in the COPO II experiments corresponds to the one in a prototypic corium pool ($\sim 10^{13}$).

This paper reports results from the COPO II-Lo and COPO II-AP experiments with homogenous pool. Results indicate that the upward heat fluxes are in agreement with the results of the COPO I experiments. Also, as expected, the time averaged upward heat flux profile was relatively flat. On the other hand, the heat fluxes at the side and bottom boundaries of the pool were slightly higher in COPO II-Lo than in COPO I. In COPO II-AP, the average heat transfer coefficients to the curved boundary were higher than predicted by Jahn's and Mayinger's correlation, but slightly lower than in BALI experiments.

INTRODUCTION

COPO II experiments are continuation of the COPO I experiments [1], which were a crucial part of the demonstration of In-Vessel Retention (IVR) concept for IVO's Loviisa NPP [2]. The basic idea in both experiments is to simulate the molten corium on the lower head of a reactor pressure vessel (RPV) in a large-scale two-dimensional slice geometry, using water as the simulant material and using volumetric Joule heating. The main difference between COPO I and II is the fact that in COPO II the boundaries of the pool are cooled till freezing so that a significantly higher temperature difference between the pool maximum and the boundaries is achieved as well as ideally isothermal boundary temperatures. The large temperature difference improves the accuracy and also allows to observe the potential effect of temperature dependent fluid properties.

In the IVR study carried out for Loviisa [2], the margins to RPV failure were found to be wide, so the refinement of the knowledge base of the heat flux distribution due to new experiments is not expected to have an impact on the overall conclusion for the Loviisa NPP. However, if the IVR is to be applied for reactors with higher power densities, further refinement of the experimental database of the corium pool behavior is deemed desirable.

Specific questions to be addressed in the COPO II -experiments are the heat flux distribution on the upper boundary (the detailed distribution was not measured in COPO I), effect of bottom geometry,
effect of non-constant fluid properties, effect of crust boundary, and heat transfer phenomena in stratified pools.

THE FACILITIES

Two geometrically different versions of the COPO II facility have been constructed: a version called COPO II-Lo, which follows the shape of the lower head of the RPV of a VVER-440 reactor (torispherical bottom) as COPO I, and a version called COPO II-AP having a semicircular shape and thus modeling the RPV bottom of a western PWR, for example AP600. A schematic of the COPO II-Lo facility is shown in Fig. 1 and a schematic of the COPO II-AP in Fig. 2.

In both facilities the molten corium pool on the lower head of a RPV is simulated by a two-dimensional slice of it in linear scale 1:2. The width of the slice is 94 mm. The pool is bounded at its periphery and at the top by aluminum cooling units, and at the lateral sides by insulated, parallel plywood walls, which are clamped tightly against the side/bottom cooler. The simulat fluid for corium is water with small amount of zinsulfate added to it. The volumetric heat generation is produced by Joule heating, i.e. by conducting electric current through the fluid itself. Maximum continuous heating power is 25 kW.

An important new feature in the COPO II facilities is the cooling arrangement in which liquid nitrogen is circulated on the backside of the aluminum walls of the pool. The use of liquid nitrogen leads to formation of ice on the inside of boundaries. Because of the ice, the boundary conditions of the pool are now ideally isothermal and, also, the temperature difference in the pool can be made sufficiently large to allow possible effects of temperature dependent fluid properties to become visible.

Heat fluxes are obtained by measuring the temperature gradients in the cooling units. The upper cooler is divided into 25 cooling units in COPO II-Lo and into 26 cooling units in COPO II-AP. The side/bottom cooler consists of 63 cooling units in COPO II-Lo and the curved boundary consists of 47 cooling units in COPO II-AP. The spatial resolution is thus 50 - 75 mm. The upper cooler is set between the lateral walls. The vertical position of the upper cooler can be varied in COPO II-Lo.

The plywood walls of the pool are equipped with quartz glass windows, which are used for monitoring the thickness of the ice at the boundaries, to record inside scenes with a video camera and for possible velocity measurements with a laser-doppler anemometer (LDA). The temperature distribution in the pool is measured with T-type thermocouples which are installed through small holes in the plywood walls.

Each experiment is started by cooling down the aluminum walls of the empty test section with liquid nitrogen in order to bring the heat transfer mode at the nitrogen-aluminum boundary from film boiling into nucleate boiling. After the walls are cooled close to the temperature of liquid nitrogen, the preheated salt water is pumped into the test section and the electrical heating is turned on. Due to thermal contraction of aluminum, at this stage of the experiments, the clamps have to be retightened. The thermal insulation is finalized and the water is allowed to circulate in the pool without the upper cooler in place in order to allow dissolved gases to escape for at least about 15 minutes. The upper cooler (precooled by liquid nitrogen) is lowered in its place and the temperature of the pool is kept at the selected temperature by adjusting the electrical power input. A steady state is reached typically within approximately an hour. The completeness of the steady state is judged from the evolving of the thermocouple (and heat flux) readings and the thickness of the ice.

Fig. 1: A schematic of the COPO II-Lo facility
EXPERIMENTS AND RESULTS

Eight tests with homogeneous (nonstratified) pool have been carried out with the COPO II-Lo [3]. With COPO II-AP four tests with homogeneous pool have been carried out [4]. The main test parameters are shown in Table 1, L and P denote COPO II-Lo and COPO II-AP, respectively. The boundary conditions for the experiments shown in the Table were isothermal (frozen boundaries) in all other experiments except for L13ad, in which the upper surface was insulated (but had a solid boundary). Values which are believed to be somewhat unreliable (e.g. due to unsatisfactory steady state, or leakages of gas) are shown in parentheses. For calculating the dimensionless numbers, the fluid properties were evaluated at the average temperature between the boundary (melting temperature of the water zincsulfate solution, about -0.2°C) and the fluid maximum temperatures. Also, the effect of the ZnSO₄ concentration was taken into account in the fluid properties.

In COPO II-Lo, the measured local heat fluxes were averaged over the pool top boundary, vertical boundary and bottom (or lower) boundary. The division between the vertical boundary and the bottom boundary was assumed between the cooling units 13 and 14 and between 50 and 51, similarly as in the published analyses of the COPO I results [1]. In COPO II-AP, the local heat fluxes were averaged over the pool top boundary and curved boundary.

Fig. 3 shows the measured average upward Nusselt numbers compared to the COPO I results [1] and to a correlation by Steinberner and Reineke [5]:

\[ \text{Nu}_{\text{up}} = 0.345 \cdot \text{Ra}^{0.233}. \]  

Also a correlation based on the first ACOPO experiments [6] is shown in Fig. 3:

\[ \text{Nu}_{\text{up}} = 1.95 \cdot \text{Ra}^{0.18}. \]  

The measured upward heat transfer coefficients are higher than predicted by the correlations but rather consistent with the COPO I results, and also with BALI results [7]. More specifically, the present results actually seem to follow a trend which is slightly higher than the results measured in COPO I with 60 cm deep pool (Ra' < 7 \times 10^{14}) but possibly somewhat below the results with the 80 cm deep COPO I (Ra' > 7 \times 10^{14}) [1]. All the COPO II result seem to fall on the same line independent of the height of the pool.

In Fig. 4, the measured average sideward Nusselt numbers in COPO II-Lo are compared to the COPO I results and to a correlation by Steinberner and Reineke [8]:

\[ \text{Nu}_{\text{sd}} = 0.85 \cdot \text{Ra}^{0.19}. \]  

The Steinberner and Reineke correlation has been obtained from experiments with rectangular test section with Ra' up to 3 \times 10^{15}. The present results seem to be clearly higher than the Steinberner-Reineke correlation and the COPO I results.

The measured average downward Nusselt numbers in COPO II-Lo are compared to the COPO I results in Fig. 5. At the side and bottom boundaries the measured heat transfer coefficients are higher than expected and exceed the values measured with COPO I.

When the upper surface of the pool was insulated, the average downward Nusselt number was remarkably lower than in the other COPO II-Lo runs with cooled upper surface. Similar decrease of the downward Nusselt numbers in pools with insulated upper surfaces was noticed also in COPO I experiments [9].

The measured average heat transfer coefficients to the curved boundary in COPO II-AP are compared to a correlation by Jahn and Mayinger [10]:

175
\[ \text{Nu}_{\text{dn}} = 0.54 \cdot Ra^{0.18} \left( \frac{H}{R} \right)^{0.26}, \]  

which is based on experiments in a 2D-slice geometry with \(10^7 < Ra < 10^{11}\). It is emphasized that the correlation in now extended beyond its original data base. The COPO II-AP results are higher than predicted by the correlation, but slightly lower than in BALI experiments \([7]\). The dependence of the average downward Nusselt numbers on the modified Rayleigh number seems to be weaker in COPO II than in BALI. However, no firm conclusions on this item can be drawn due to the limited number of data points and the uncertainty band of the results.

The measured heat flux profiles at the upper boundary are shown in Fig. 7. As expected, the profiles are rather flat, the deviation from the average value is about 10%. The heat flux profiles at the vertical boundary are shown in Fig. 8. Unlike in COPO I, the sideward heat flux seems to be slightly peaked just below the upper surface of the pool. However, this apparent finding should be interpreted very cautiously, since possible inaccuracies in the location of the upper surface - vertical surface joint as well local heat losses may have disturbed the results. Obviously, when the upper surface is insulated, peaking is very strong (Fig. 10) due to strong temperature stratification of the pool. This is also consistent e.g. with the results by Asfia and Dhir \([11]\).

At the very bottom of the pool, the heat fluxes measured in COPO II-Lo are somewhat higher than in COPO I (Fig. 9). This could be, at least partly, explained by the fact that in COPO II, the boundary temperatures are strictly isothermal, which, for the bottom part of the pool, was not exactly the case in COPO I. Another potential reason is the thick ice layer (-5 cm) in which the heat can be conducted also in the direction parallel to the wall.

Table 1: Main parameters of the COPO II experiments

<table>
<thead>
<tr>
<th>Run</th>
<th>(P_{\text{input}}) (kW)</th>
<th>(q''_{\text{up}}) (kW/m²)</th>
<th>(q''_{\text{sd}}) (kW/m²)</th>
<th>(q''_{\text{dn}}) (kW/m²)</th>
<th>(T_{\text{pool, max}}) (°C)</th>
<th>(H) (mm)</th>
<th>(Ra)</th>
</tr>
</thead>
<tbody>
<tr>
<td>L5</td>
<td>11.8 (40.9)</td>
<td>44.5</td>
<td>14.9</td>
<td>58.4</td>
<td>664</td>
<td>6.4 \times 10^{14}</td>
<td></td>
</tr>
<tr>
<td>L6</td>
<td>13.7 (34.1)</td>
<td>53.8</td>
<td>(19.5)</td>
<td>63.7</td>
<td>639</td>
<td>6.3 \times 10^{14}</td>
<td></td>
</tr>
<tr>
<td>L7</td>
<td>18.9 (57.7)</td>
<td>(47.5)</td>
<td>(12.4)</td>
<td>54.6</td>
<td>730</td>
<td>9.5 \times 10^{14}</td>
<td></td>
</tr>
<tr>
<td>L8</td>
<td>15.5 (62.0)</td>
<td>38.9</td>
<td>14.5</td>
<td>55.4</td>
<td>708</td>
<td>8.6 \times 10^{14}</td>
<td></td>
</tr>
<tr>
<td>L10</td>
<td>9.7 (36.7)</td>
<td>37.1</td>
<td>11.6</td>
<td>43.6</td>
<td>(685)</td>
<td>3.7 \times 10^{14}</td>
<td></td>
</tr>
<tr>
<td>L12</td>
<td>15.0 (57.4)</td>
<td>39.6</td>
<td>13.2</td>
<td>55.7</td>
<td>702</td>
<td>8.0 \times 10^{14}</td>
<td></td>
</tr>
<tr>
<td>L13</td>
<td>14.5 (55.1)</td>
<td>31.1</td>
<td>11.0</td>
<td>51.3</td>
<td>847</td>
<td>1.4 \times 10^{15}</td>
<td></td>
</tr>
<tr>
<td>L13ad</td>
<td>5.8</td>
<td>39.8</td>
<td>79</td>
<td>64.3</td>
<td>864</td>
<td>8.8 \times 10^{15}</td>
<td></td>
</tr>
<tr>
<td>P4a</td>
<td>20.5 (68.3)</td>
<td>-</td>
<td>25.1</td>
<td>57.7</td>
<td>932</td>
<td>3.8 \times 10^{15}</td>
<td></td>
</tr>
<tr>
<td>P4b</td>
<td>9.5 (32.1)</td>
<td>-</td>
<td>(14.7)</td>
<td>37.8</td>
<td>912</td>
<td>(1.0 \times 10^{15})</td>
<td></td>
</tr>
<tr>
<td>P6a</td>
<td>16.4 (47.8)</td>
<td>-</td>
<td>21.2</td>
<td>49.3</td>
<td>960</td>
<td>2.6 \times 10^{15}</td>
<td></td>
</tr>
<tr>
<td>P6b</td>
<td>8.7 (24.0)</td>
<td>-</td>
<td>13.3</td>
<td>33.6</td>
<td>940</td>
<td>7.5 \times 10^{14}</td>
<td></td>
</tr>
</tbody>
</table>
Fig. 3: Average upward Nusselt numbers compared to the COPO I results and to ACOPO and Steinberner-Reineke correlations.

Fig. 4: Average sideward Nusselt numbers in COPO II-Lo compared to the COPO I results and to Steinberner-Reineke correlation.
Fig. 5: Average downward Nusselt numbers in COPO II-Lo compared to the COPO I results

Fig. 6: Average downward Nusselt numbers in COPO II-AP compared to the BALI results and to Jahn’s and Mayinger’s correlation
Fig. 7: Distribution of heat flux on the upper boundary of COPO II-Lo for three typical runs

Fig. 8: Distribution of heat flux on the vertical boundary of COPO II-Lo for three typical runs
**Fig. 9:** Distribution of heat flux on the curved boundary of COPO II-Lo for three typical runs compared to the heat flux profile measured in COPO I

**Fig. 10:** Effect of upper surface cooling to the heat flux profile
DISCUSSION

In general, the average heat transfer coefficients measured in the COPO II tests seem to deviate somewhat from what might have been expected based on earlier experiments such as Steinbemer and Reineke, COPO I and ACOPO.

As for the upper surface, the measured average heat transfer coefficients were found to be rather consistent with the COPO I and BALI results. However, they were higher than predicted by the widely used correlation by Steinbemer and Reineke, and also higher than measured in the ACOPO experiments [6]. Reason for the discrepancy between COPO II and ACOPO has been searched, but no definite answer exists today. However, some of the potential explanations are:

- Roughness of the ice surface which is not present in the non-crust experiments (ACOPO). In COPO II the ice at the boundaries was found not to be smooth, but to exhibit bumps, or irregular waves, which at the upper boundary had height of typically several millimeters or even close to one centimeter. The distance between the bumps was typically several centimeters. However, this explanation does not seem to be supported by the COPO I results.

- 2D (COPO II) vs 3D (ACOPO) effects. It could be speculated that in a 2D case the flow patterns below the upper surface could be more structured and regular thus enhancing the heat transfer.

- Effect of the temperature dependent fluid properties. If the fluid properties were evaluated at the temperature of the bulk of the fluid (instead of the film temperature, which is assumed to be the average between the pool maximum and the boundary temperature), the results would match with the Steinbemer and Reineke correlation. For the Steinbemer-Reineke and for COPO I the difference between the bulk and film temperatures is small, and therefore, the results from them are not sensitive to the definition of the reference temperature. However, in ACOPO, the temperature difference is similar or even larger than in COPO II. This hypothesis would, thus, not explain the discrepancy to ACOPO, as the ACOPO results shown in Fig. 3 are evaluated also at the film temperature. The effect of the reference temperature used for evaluating the fluid properties is demonstrated in Fig. 11.

![Fig. 11: Effect of reference temperature on the average Nu at the upper surface. "Hot" refers to the fluid properties being evaluated at the bulk temperature instead of film temperature](image)

At the side and bottom boundaries the measured heat transfer coefficients in COPO II-Lo are clearly higher than measured in COPO I and predicted by the Steinbemer and Reineke correlation. Comparing COPO I and II, it may be noted that the only major difference is the fact that the boundaries in COPO II are frozen and that the temperature difference from the bulk of the fluid to the boundary is several times larger than in COPO I. Similarly as at the upper boundary, the ice at the side and bottom boundaries was also not smooth, but showed bumps, which at the side/bottom boundaries were partly even larger than at the top boundary. Particularly, at the bottom of the pool, the surface of the ice was very bumpy, with amplitude exceeding one centimeter. (This was also enhanced by additional blocks of teflon used at the bottom as a thermal resistance to reduce the thickness of the ice). It is conceivable that the downward flowing thin boundary layer could be affected by the structure of the ice surface and that the heat transfer coefficient would therefore be increased.

Regarding heat flux profiles at the side and bottom boundary, it was found that in COPO II-Lo the heat fluxes at the very bottom of the pool are somewhat higher than in COPO I. This could be, at least partly, explained by the fact that in COPO II, the boundary temperatures are strictly isothermal, which, for the bottom part
of the pool, was not exactly the case in COPO I. Another potential reason is the thick ice layer at the very bottom in which the heat can be conducted also in direction parallel to the wall.

CONCLUSIONS

The COPO II experiments with a homogenous 2-dimensional pool with the modified Rayleigh number up to 4·10^{15} have been discussed in this paper. In the experiments, crust (ice) was formed at all boundaries.

The upward heat transfer coefficients measured seem to be in accordance with the earlier COPO I experiments but exceed the heat transfer coefficients predicted by the well-known correlation by Steinbner and Reinke and also those measured in the three-dimensional ACOPO experiment.

On the other hand, the heat transfer coefficients at the side/bottom boundaries were found to be higher in COPO II-Lo than in COPO I, in which no ice on the boundaries existed.

Future tests with COPO II, in which large temperature without frozen upper boundary is employed is expected to clarify reason of the discrepancy between the results from different facilities.

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NOMENCLATURE

\[ \alpha \quad \text{thermal diffusivity (m}^2/\text{s}), = \frac{\lambda}{c\rho} \]
\[ \beta \quad \text{thermal expansion coefficient (1/K)} \]
\[ \gamma \quad \text{slope of the boundary (deg)} \]
\[ \Delta T \quad \text{difference between the maximum temperature of the pool and the boundary temperature (°C)} \]
\[ \eta \quad \text{dynamic viscosity (Pa·s)} \]
\[ \lambda \quad \text{thermal conductivity (W/(m·K))} \]
\[ \nu \quad \text{kinematic viscosity (m}^2/\text{s}), = \frac{\eta}{\rho} \]
\[ \rho \quad \text{density (kg/m}^3) \]
\[ c \quad \text{specific heat (J/(kg·K))} \]
\[ H \quad \text{height of the pool (m)} \]
\[ P \quad \text{power (W)} \]
\[ Q \quad \text{volumetric heat generation (W/m}^3) \]
\[ q' \quad \text{heat flux (W/m}^2) \]
\[ R \quad \text{radius of curvature (m)} \]
\[ T \quad \text{temperature (°C)} \]
\[ \text{Nu} \quad \text{Nusselt number,} \quad \frac{q H}{\Delta T \lambda} \]
\[ \text{Pr} \quad \text{Prandtl number,} \quad \nu = \alpha \]
\[ \text{Ra} \quad \text{modified Rayleigh number,} \quad = \frac{g Q \beta H^5}{\alpha c \lambda} \]

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Thermalhydraulic Phenomena in Corium Pools: Numerical Simulation with TOLBIAC and Experimental Validation with BALI.

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Summary
In the frame of severe accidents studies, the behavior of corium pools is simulated by the TOLBIAC code. After a short description of the model and peculiarities of the code, its capacities are illustrated with results of the simulation of the behavior of a corium pool in a core catcher made of concrete. The BALI experiments and first results are then presented, and finally BALI tests simulation with TOLBIAC.

1. Frame
In the frame of severe accidents studies, the behavior of corium pools has to be predicted. Numerous physical processes are involved in these studies, and a predictive tool is of great interest in order to investigate several situations, geometries or events.

The TOLBIAC code is devoted to the simulation of the thermalhydraulic behavior of a corium pool in in-vessel or ex-vessel situations, with natural convection.

The objective of the BALI experiments is to obtain data in situations encountered in this frame, in order to investigate the physical phenomena and to assess the TOLBIAC code.

This work is performed within the frame of a CEA, EDF and FRAMATOME agreement. The BALI experiments are also included in the frame of 4th P.C.R.D in the M.V.I. project.

2. Description of the TOLBIAC code
The three fields model
The metal liquid phase is a mixture of iron, zirconium, nickel and chromium, in proportions defined by the code user; it is characterized by its own volumetric fraction, temperature and velocity fields.

The oxide liquid phase is characterized by its own temperature and velocity fields. It is divided into light and heavy oxides, each one with its own volumetric fraction. The heavy oxides are a mixture of uranium dioxide and zirconia. The light oxides, issued from the wall ablation, are a mixture of alumina, silica and lime, or other materials, depending of the nature of the wall.

Finally the gas phase, characterized by its volumetric fraction and velocity fields, is a mixture of carbon dioxide and water, issued from the concrete wall ablation.

To sum up, four mass balance equations are used (metal, oxide, light oxide and gas), together with two energy balance equations (metal and oxide; the gas temperature is taken equal to the surrounding liquid temperature), and three momentum balance equations (metal, oxide and gas).

A diffusion term is included in the energy and momentum equations, taken into account viscosity and conductivity effects, but no peculiar turbulence model.

Wall, pool surface, ablation and crusts
Apart from the hydraulic equations, the wall temperatures are calculated either using 1D conduction equations, or by solving the conduction equations with a 2D meshing for the entire wall. The conduction equations take into account the melting heat and give directly the ablation velocity. A moving fine meshing of the wall is needed in order to obtain the relevant wall temperature profiles and ablation velocity.

At the pool surface either radiative heat transfer or heat transfer with water are simulated.
The crust formation is calculated at the wall and at the free surface. Crusts appear if the wall temperature or surface temperature is lower than the solidification temperature, taken as the
liquidus temperature. The crust thickness is calculated by means of a heat balance in the crust, with an hypothesis of a stationary temperature profile in the crust.

**Numerical features**

The equations are discretized with a finite difference scheme on a staggered spatial meshing, and with the donor-cell method. The numerical integration uses a semi-implicit method. The non-linear system of equations is solved through a Newton-Raphson iteration procedure.

**Physical properties and constitutive laws**

The physical properties of the corium components are not well known. The main parameters are the thermal conductivity of the liquid metals, the viscosity of the oxides and the liquidus temperature of the oxides. The properties of the oxides and metals are classically deduced from the values of each material with a mass or volume balance. The constitutive laws are correlations from the literature, or simple models with consistent trends.

3. **Simulation of the corium behavior in a core catcher**

In order to illustrate the code capacities, some results are presented here (fig. 1), concerning the behavior of the corium in an external core catcher with concrete walls.

At the beginning the corium pool is stratified, with a layer of metals at the top, and heavy oxides in the bottom. The concrete ablation generates light oxides, which mix with the heavy oxides, and gas. The gas generation destroys the stratification. Moreover, when enough light oxides are mixed to the heavy oxides, the metals become heavier than the oxides. When all the concrete is molten (no more gas generation) the final state is obtained with inverse stratification (oxides at the top, metals in the bottom). Fig 1 presents the volumetric fraction of metal in the core catcher at different times.

![Diagram showing volumetric fraction of metal in the core catcher at different times](image)

**Fig. 1:** Field of the volumetric fraction of metals at different times, in a core catcher with concrete walls

These results illustrate the capacity of the TOLBIAC code to deal with 3 phases situations. They are at present time not validated: some models are missing (oxidation reactions) or very simplified and
not assessed (wall heat transfer in presence of gas generation, interfacial shear stress between gas and the liquids in the pool).

The BALI tests are used to assess the code capacity to simulate natural convection situations in a pressure vessel, with a single component.

4. Description of the BALI experiments

The BALI program has been designed to study the thermal-hydraulics of corium pool for in-vessel or ex-vessel situation. The corium melt is represented by salted water and the lower head or the core catcher by a slice at scale 1:1 of constant thickness (15 cm). These dimensions provide values of internal Rayleigh number \( Ra \) of \( 10^{16} \) to \( 10^{17} \), for lower head geometry, matching those in the prototypic situation for French P.W.R.

The pool is cooled from the bottom and the top and heated electrically by Joule effect with current supplies located on the sides. The coolant is an organic liquid which may be used with a temperature ranging from 0°C to -80°C, thus an ice crust forms at the pool boundaries to provide a constant temperature boundary condition (fig. 2). For M.C.C.I. ex-vessel situation, gas can be injected through porous wall.

The measurements consist of heat flux distributions over the pool boundaries and axial temperature distributions in the pool. Velocity fields may also be measured by P.I.V. process. The test matrix includes variations of the water height, power density, water viscosity, pool porosity, cooling conditions and superficial gas velocity.

First experimental results

The objective of the first test campaign was to analyze the effect of internal Rayleigh number on heat transfer in the range \( 10^{16} \) to \( 10^{17} \) to extend the validity range of previous experimental correlations. Twelve BALI tests have been run with 1,5 and 2,0 meter height geometry, for different power densities and for two of them without top cooling.

In order to compare our results with COPO or ACOPO experiments we use the following definitions. For Nusselt and Rayleigh calculations, the physical properties are taken at film temperature: \( (T_{\text{bulk}} + T_{\text{wall}})/2 \). The Nusselt numbers are calculated from thermal balances and maximum temperature difference between bulk and wall. Moreover, the bulk temperature is defined as the average temperature in the upper unstable layer which is at maximum temperature. The length scale is based on the height of the pool \( H \).

\[
\overline{Nu} = \frac{PH}{SA \Delta T_{\text{max}}} = \frac{\overline{hH}}{\lambda} \quad \text{with} \quad \Delta T_{\text{max}} = T_{\text{bulk}} - T_{\text{wall}}
\]
For upward average heat transfer (fig 3), the BALI Nusselt numbers are smaller than the values extrapolated from the COPO I results [1]. Nevertheless, a better agreement is observed with COPO II results [3] in which wall temperature is also imposed by ice crust formation. However, the 2D BALI results are 30% higher than the 3D ACOPO results [2].

**Upward heat transfer  ACOPO - BALI - COPO I & II Comparison**

![Average Nusselt number for upward heat transfer vs. Internal Rayleigh number](chart.png)

**Fig. 3: Average upward heat transfer.**

From BALI and COPO II results, a correlation for upward heat transfer has been derived in the prototypic range for 3D reactor applications:

\[
\overline{Nu_{up}} = 0.736 Ra_i^{0.216} \quad \text{with} \quad Ra_i = \frac{g \beta Q H^5}{\lambda \nu \alpha}.
\]

For downward heat transfer, no comparison has been performed with COPO results in which a distinction is made between sideward and downward heat transfer due to the specific elliptic geometry.

The 2D BALI downward average Nusselt numbers are still 20 to 30% over the 3D ACOPO results (fig. 4). The following correlation is derived from experimental results.

\[
\overline{Nu_{dn}} = 0.123 \left( \frac{H}{R} \right)^{0.35} Ra_i^{0.25} \quad (2D)
\]

Examples of temperature profile on the axis of the pool, and heat flux profile on the curvilinear wall are presented in fig 7-8 and compared with Tolbiac results. For upward heat transfer, as local heat flux distribution is uniform, local and average heat transfer coefficient values are the same. For downward heat transfer, a good agreement is obtained when we compare local measured heat exchange coefficients with those calculated from local temperature difference and correlation in turbulent regime derived from Chawla and Chan [5].

\[
Nu_s = 0.19 Ra_s^{1/3} \quad \text{with} \quad Nu_s = \frac{h_s x}{\lambda} \quad \text{and} \quad Ra_s = \frac{g_s \beta \Delta T_s x^3}{\nu \alpha}
\]

The ratio between maximum local heat flux and downward average is around 1.8.

Concerning the flow pattern, we have observed three different regions: upper unstable region, lower stratified region including a boundary layer flowing downward along the curvilinear wall. Due to the
effect of top cooling, the upper layer is very unstable: periodically, a denser cold fluid is released from a tiny sublayer. In this well mixed region, (velocities around 1 cm/s but zero in average values) the temperature is uniform and reaches its maximum value. In the stratified region, the flow is very stable with an upward velocity smaller than 1 mm/s.

The difference between BALI-COPO and ACOPO experiments need more detailed analysis. Concerning this point, it is not possible to give, at present, a complete explanation, and we can just propose a few hypotheses.

First of all, it is important to notice that if the Nusselt numbers are different in absolute values their ratios $\text{Nu}_{\text{up}}/\text{Nu}_{\text{dn}}$ are quite similar. This is very important for reactor applications, because it means that the ratio between the power extracted upward and the residual power is the same in both case: around 43% for $H/R=1$. Just the maximum temperature of the pool would be different, which is not so important for in vessel situation.

To explain the differences, we first assume that there might be a translation in Nusselt and Rayleigh numbers. This may find its origin in experimental uncertainties on heat balances, in reference temperature used to calculate physical properties (but it seems that same definitions have been taken) or in the location where the pool temperature is measured.

Secondly, we may also assume that, due to the concept of ACOPO experiment, the transient cool-down can introduce a deviation between steady-state and transient temperature distribution in the pool and in the wall. This effect will be investigated further.

Third, the geometry effect (2D in BALI, 3D in ACOPO) has been investigated. For this investigation we assume that upward and downward local heat transfer coefficient remains the same in 2D or 3D geometry. The wave lengths of observed thermal instabilities are more than 10 times smaller than the smallest dimension of the test section and the large scale motion observed in hard turbulence regime, which is certainly different in 2D and 3D, has not a great influence on the heat transfer.

Calculations have been made for 2D and 3D geometry with the same local heat transfer coefficients. If no differences are observed for upward heat transfer where the heat flux is uniform, for downward heat transfer the correlation obtained for heat transfer in average values is just a little bit greater in 3D.
\[ \text{Nu}_{de} = 0.135 \left( \frac{H}{R} \right)^{-0.41} \text{Ra}_i^{0.25} \]  

(3D)

Consequently, the direct effect of geometry cannot explain the reduction observed for ACOPO Nusselt numbers.

**Metallic layer and focussing effect**

Specific tests have been carried out by the end of 1997 on 2 meters wide, 15 cm thick rectangular BALI test section to study the effect of heat flux concentration in a metallic layer for stratified pool. The metallic layer is reproduced by water layer heated from the bottom with heating plate allowing uniform and non uniform heating, cooled from side by change of state allowing ice crust formation, and cooled from the top by plastic heat exchanger with coolant at 0°C. The thermal resistance of plastic exchanger can reproduce the equivalent thermal resistance of a realistic heat transfer by radiation. The scaling of the experiment respect Grashof numbers and the ratio between global upward heat transfer coefficient and sideward heat transfer coefficient.

Different tests have been made for 5 to 40 cm height with uniform or non uniform bottom heat flux (no power injected in the first 40 cm near the cooled wall in case of non uniform heat flux). From the first analyses of the results, we observe (fig. 5) in both case concentration factors (ratio between average sideward heat flux over average bottom heat flux) higher than 4 in 2D geometry (which means higher than 2 in 3D geometry) with one large convection cell.

![2D focussing effect](image)

Fig. 5: Concentration factor for 2D Geometry

For 10 and 5 cm height, a radial temperature gradient appears in the pool (fig. 6) where one large convection cell changes periodically to several small convection cells. Note that these convection cells have to be understood more as large scale motion in a turbulent unstable flow than as classical convection cells in laminar Rayleigh-Bénard convection regime.

The experiments show that for shallow layers (5cm, 10cm) a radial temperature gradient appears in the fluid layer which reduces the focussing effect. However this reduction is not very important.
(around 15% reduction in lateral heat flux for a thickness of 5cm) and is not sufficient to suppress the focussing effect.

Fig. 6: Example of temperature profiles (H=10cm uniform 2 kW heating)

5. BALI simulation with TOLBIAC

In order to validate the heat transfer laws used in the lower head application of TOLBIAC simulation code, we compare the BALI results with the heat flux distributions at the pool boundaries and the temperature profile along the vertical axis of the pool, calculated by TOLBIAC.

In BALI, the salt concentration is so small that we consider it does not change the thermalhydraulic properties of the water. However, we take into account the position of the electrodes (reduced volumetric heating close to the boundaries of the pool), and the evolution of the volumetric power with the temperature. Ice formation at the boundaries is also reproduced with the crust model of TOLBIAC.

Several heat transfer laws in natural convection are tested:

- upper boundary : Kulacki and Emara (4), and the law established in order to fit the BALI and the COPO II tests.
- lateral boundary : Chawla and Chan (5), and the local law deduced from the BALI tests.
- lower boundary : Chawla and Chan (5), Fishenden and Saunders (6), and the law deduced from the BALI tests.

The thermalhydraulic properties used for these laws are calculated at the film temperature.

We choose seven of the BALI tests, with different test conditions:

- height of the pool : from 1.46 m to 2.0 m.
- upper boundary condition : cooled or adiabatic.
- internal Rayleigh number : from $3.10^{15}$ to $7.10^{16}$ depending on the power dissipated in the pool.

For the tests with no heat transfer at the top, a peculiar meshing has to be used in the TOLBIAC simulation. Instead of the regular meshing used for the tests with top cooling, a fine mesh along the
lateral wall is needed (cell size of 2 mm at the wall instead of 50 mm). The downward flow along the lateral wall is concentrated in a small layer. The pool is entirely thermally stratified (fig. 7).

Fig. 7: TOLBIAC simulation of a BALI test without top cooling.

For the tests with top cooling, the upper instabilities lead to a flow configuration quite different. A well mixed region appears in the third upper part of the pool, and the lower part of the pool is thermally stratified (fig. 8). The simulations with a regular meshing gives a flow configuration similar to the observed one.

Fig. 8: TOLBIAC simulation of a BALI test with top cooling.
Concerning the heat transfer laws, the best fit for the temperature profile is obtained with the Bali_up law for the upper boundary, the Bali_side law for the lateral one, and the Fishenden and Saunders law for the lower one.

Even if the temperatures are systematically over-predicted in the lower part of the pool, the results are not very sensitive to the correlation used for the lower boundary: the main heat transfer occurs in the third upper part of the pool.

Moreover, the Kulacki and Emara law for the upper boundary seems to be not valid for such a range of Rayleigh numbers and the agreement is good for the Bali_up law.

For the sideward and downward heat transfers, the Chawla and Chan correlation seems to over-predict the temperature in the pool. The local Bali_side law is in better agreement.

6. Conclusion

The TOLBIAC code is devoted to the simulation of the thermalhydraulic behavior of corium pools. Three velocity fields and two temperature fields are used. Wall ablation and crust formation are taken into account.

The TOLBIAC code is able to simulate the behavior of a corium pool in a core catcher with concrete wall generating gas. However the code models are not yet qualified for such a configuration.

The TOLBIAC code is able to simulate the BALI tests, with heat transfer correlations using physical properties calculated at the film temperature.

For the tests without top cooling, a fine mesh at the wall is needed in order to simulate a fine recirculating layer.

For the tests with top cooling, the upper well mixed zone is obtained with a regular meshing.

The BALI tests were performed with water in a geometry representative of a pressure vessel for homogeneous pool and for metallic layer. For homogeneous pool, a good agreement is observed with COPO II experiments. Experimental correlations are derived for 2D and 3D application, but the results are still greater than the 3D ACOPO results. Nevertheless, for reactor point of view we arrive to the same conclusion: the ratio between power extracted from above and residual power is 43% for H/R=1. For metallic layer, if the apparition of thermal gradient for shallow layer reduces the focussing effect this reduction is not very important and not sufficient to suppress the problem.

References


Tolbiac Code Simulations of some Molten Salt RASPLAV Experiments
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Summary

In the frame of severe accidents studies, the behaviour of a corium pool has to be predicted. Numerous physical processes are involved in these studies, and a predictive tool is of great interest in order to investigate several situations, geometries or events. The TOLBIAC code, developed by CEA/Grenoble, is devoted to the simulation of the thermalhydraulic behaviour of a corium pool both in-vessel and ex-vessel situations, with natural convection.

Some molten salt RASPLAV experiments have been simulated for the TOLBIAC code assessment. An adiabatic condition at the free surface of the molten pool is used. The comparisons between TOLBIAC calculations and RASPLAV data concern the pool temperatures, the local wall heat fluxes and the crust thickness (if any). The wall heat transfer correlations are the key parameter for the agreement between calculations and data, and are discussed. Concerning the crust thickness, the results are sensitive to their physical properties, and mainly to the thermal conductivity.

1. INTRODUCTION

TOLBIAC code bidimensional (2D) calculations have been carried out of some molten salts SidedWall Heated (SWH) Rasplav experiments [1]. TOLBIAC code is described elsewhere [2]. ENEA has TOLBIAC code available through the ENEA-CEA R&D agreement on future nuclear reactors [3]; Tolbiac code is developed by CEA-Grenoble in the frame of an agreement between CEA, EDF and Framatome. Two tests with wholly liquid molten pool (no-crust tests) at two different power levels and one test with crust formation (crust-test) have been simulated. The simulation concerns one half test section (from the pole to the equator). The test wall outer temperature (Touter), derived from the experimental data, is used as a boundary condition. An adiabatic upper boundary condition is assumed at the molten pool free surface. In default of complete informations on the selected experiments at the moment of the preliminary calculations it was necessary to do some calculational assumptions: the pool height (Hp) value was assumed identical for the three examined cases; the values of the power injected into the molten pool were approximately evaluated.

The informations available after the completion of the preliminary calculations showed that the assumed Hp values were lower than the experimental values in the two no-crust tests, the calculational power levels were lower (20-30%) than the experimental ones for the three examined cases. The right assumption have been kept in the here also referred final calculations.

The comparisons calculation-experiments concern:
- the test wall local heat fluxes (HF) angular variation;
- the pool temperatures;
- the crust thickness (if any) angular variation.
2. NO-CRUST TESTS SIMULATION

There is more than a factor of two in the assumed power levels of the two no-crust tests (low-power and high-power case). The comparison between the experimental (for negative as well as for positive angles) and calculational heat flux distribution along the test wall is shown in Fig. 1a for the low-power case and in Fig. 1b for the high-power case; the heat flux values are normalized to the experimental heat flux at the pole \( (HF/HF_{\text{exp}}(0^\circ)) \). The heat flux calculated values, in particular in the final calculations, are higher than the experimental ones approaching the equator. There are also differences near the pole, in particular in the low-power case, being the calculated values lower than those observed in the tests.

As far as the pool temperatures are concerned, Tolbiac results show a molten pool warmer than that experimentally observed; see Fig. 2a and Fig. 2b (the temperature along the normalized pool height \( (H/H_p) \) at the test section center line) and Fig. 3a and Fig. 3b (the molten pool temperature field derived in the final calculations); the temperature values are normalized to the experimental test wall inner temperature at the pole \( (T_{\text{in,exp}}(0^\circ)) \). The thermocouples (TC) \( T_{\text{ij}} \), used to measure molten pool temperature field, are rigidly mounted to a light movable frame [1]; they are placed at positive angles, TC+, as well as at negative angles, TC-. TC- (their tick marker is \( V \)) are shown in Fig. 3a and Fig. 3b at their symmetrical positions, because Tolbiac geometric simulation concerns only one half test section (see paragraph 1). This representation shows that most TC+ and TC- are symmetrically placed respect to the test section center line; it gives also quick information about the symmetry of the performed tests.

The differences in the calculated-experimental temperatures are of order of few percents, increase increasing the power and are therefore higher in the final calculations due to the power levels higher than the previous assumed values. It is therefore to investigate if a better agreement calculations-experiments is possible modifying, for example, the molten pool-test wall heat transfer correlations used at present in the code; in this direction it may be of interest the use in the TIBJAC code of the correlations obtained in the framework of RASPLAV Phase I molten salt experiments. For the present simulations the Churchill and Chu [4] heat transfer correlation is considered for the vertical wall and the Haberstroh and Reinders correlation [5] for the horizontal wall with a smooth interpolation between the two correlations for the intermediate angles.

3. CRUST TEST SIMULATION

The crust thickness \( (S) \) depends strongly from some key parameters; among these the crust thermal conductivity \( (K_{\text{cr}}) \). Two preliminary calculations have been carried out with two \( K_{\text{cr}} \) values, \( K_{\text{cr}}=K \) and \( K_{\text{cr}}=2\times K \); the higher value is the \( K_{\text{cr}} \) value estimated by Russian team [1] and also used in the final calculation. The calculated crust thickness along the test wall is shown in Fig. 4; the thickness values are normalized to the calculated peak value in the \( K_{\text{cr}}=K \) case \( (S/\text{Scal},k) \). The crust is rather thick near the pole and decreases substantially moving towards the equator. No experimental quantitative informations were available at the time of the preliminary calculations about crust thickness along the test wall. In default of these informations, some considerations were done by TC readings. The crust is along all the test wall; the \( T_{\text{in,exp}} \) value is everywhere lower than the salt solidification temperature \( (T_{\text{sol}}) \). In Fig. 4 TC- positions are also shown having temperature \( (T_{\text{sol}}) \). In Fig. 4 TC- positions are also shown having temperature lower than (empty tick marker) and greater than (full tick marker) \( T_{\text{sol}} \); the empty (full) tick marker gives consequently information about presence of solid (liquid) salt at its own
position. The calculated crust distribution on the test wall seems qualitatively similar to the experimental one. On the other hand, the comparison on the maximum thickness value is poor from the quantitative point of view in the Kcr-K case: the experimental value is at least 2.5 times the Tolbiac calculated value: TC-, placed near the pole at about 2.5 times the calculated crust thickness far away from the test wall, shows a temperature lower than Tsol and, consequently, is embedded in the crust. A good agreement is in the Kcr=2*K case.

The crust thickness obtained in the final calculation is also shown in Fig. 4 as well as that recently available and derived by indirect measurements, based on the experimental thermal flux [1]. The differences calculation-experiment concerning the maximum crust thickness are related to the differences in the calculational-experimental thermal flux at the pole (see Fig. 5), since the crust thickness is in inverse proportion to the heat flux. Informations on the experimental errors concerning the TC positions as well as the heat flux should be certainly helpful for a better comparison calculations-experiment.

As far as the comparison between the experimental and calculational heat flux distribution (Fig. 5) is concerned, the values in the preliminary calculations are higher than the experimental ones near the pole and they become lower approaching the equator, the differences being higher in Kcr=K case; in the final calculation the heat flux is everywhere higher. Furthermore, the heat flux and the crust thickness being strictly related to each other, the Tolbiac code foresees partially what experimentally observed: the presence of the thick crust reduces the heat transfer strongly near the pole and results in a redistribution of the local heat fluxes; therefore the pick values experimentally observed near the equator (Fig. 5, HFmax/HFmin= 23) are considerably higher than those observed in the no-crust cases (Fig. 1b, HFmax/HFmin= 14).

As far as the pool temperature is concerned (Fig. 6, the temperature along the pool relative height at the test section center line) the TOLBIAC values are slightly higher than what experimentally observed, reducing the differences near the molten pool free surface; the temperatures, here, are normalized to the Tsol value. Any way, these differences are more relevant for the final calculation results. Fig. 6 shows also that the calculated crust thickness is smaller than the experimental one, particularly in Kcr=K case: the TC at a distance from the pole greater than the calculated maximum thickness reads a temperature value lower than Tsol.

4. ADDITIONAL FINAL CALCULATIONS

Two further final calculations have been carried out concerning the no-crust high power level case. In the first calculation the adiabatic boundary condition assumed till now is replaced by heat exchange by radiation at the melt surface; the total power injected into the molten pool is increased of 17% (it is the upwards pool heat loss experimentally determined) in order to have the heat flow through the test wall identical to that considered in the adiabatic case (the experimentally determined radiation heat outflow in a majority of SWH tests did not exceed 15-17% of the total input power [1]). A second calculation has been carried out with 32x30 meshes (all the above mentioned calculations have been done with 21x22 rectangular meshes) in order to evaluate the influence of the spatial meshes; TOLBIAC calculations carried out in [2] show indeed a significant dipendence of the results from the spatial meshes near the test wall in proximity of the molten pool free surface in case of adiabatic upper boundary condition. The results in both cases (see Figs. 7) show differences in the pool temperature distribution. The temperatures in the pool upper part are higher in the thin
meshes case and in the thermal radiation case lower than those observed in the adiabatic case. As far as the comparison experiment calculation is concerned, even if the simulation with top cooling results in the temperature values slightly higher than the experimental ones, it gives Anyway a flat temperature profile at the top of the pool similar to that experimentally observed; therefore, a better simulation seems to be obtained with top cooling.

5. CONCLUSIVE REMARKS

The herein referred results show TOLBIAC code is able to reproduce the test section thermal behaviour observed at the RASPLAV-A-Salt Facility. Some differences have been however noticed; the modifications of the molten pool-test wall heat transfer correlations may be a first step for a better agreement between calculational and experimental results. In this direction it may be of interest the use in the TOLBIAC code of the correlations obtained in the framework of RASPLAV Phase I molten salt experiments. The changes in code correlations have obviously also to be confirmed by the simulation of tests carried out in other similar experimental programmes, as for example those referred in [2]. Moreover, the availability of the RASPLAV results of molten salt tests carried out with Direct Electrical Heating (DEH) [1] will be useful for the assessment of TOLBIAC code that at the actual stage of development is more suitable for the simulation of DEH than SWH tests. Finally, a better knowledge of some material physical properties (as, for example, measurements of the crust thermal conductivity) should be highly helpful.

REFERENCES

Fig. 3a - Low-Power Case: Pool Temperature Field

Fig. 3b - High-Power Case: Pool Temperature Field
Fig. 4 - Crust-test Simulation
Crust Thickness and Termocouples Values

Fig. 5 - Crust-test Simulation
Heat Flux Angular Distribution
SIMECO Experiments on In-Vessel Melt Pool Formation and Heat Transfer
with and without a Metallic Layer

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Abstract

This paper describes an experimental program performed at the Division of Nuclear Power Safety, Royal Institute of Technology (RIT/NPS) on a facility named as SIMECO (Simulation of In-vessel MELt COolability). The objectives of the experiments in the SIMECO facility are to investigate (i) the effect of boundary crusts and mushy layers on natural convection heat transfer; (ii) the effects of melt stratification on natural circulation; (iii) the amelioration of melt stratification by turbulent flow fields, and finally (iv) the multidimensional heat transfer in, and between, the melt pool, the top metallic layer and the vessel. Step by step integral experiments are planned.

The SIMECO is a slice-type facility with a semicircular section and a vertical section. Diameter, height and width of the test section are, respectively, 530x620x90mm. Binary salt mixtures are employed as oxide melt simulant, with appropriate molten metals as metal-layer simulants.

Results of SIMECO tests performed are presented, and compared to existing correlations. Results of analyses performed with the MVITA code are compared to the data obtained.

1 Introduction and background

A hypothetical core melt accident in a light water reactor (LWR) may result in accumulation of core debris in the lower head of the reactor pressure vessel (RPV). The core debris, if unquenched, may heat up and commence natural circulation. The core melt pool formed likely will consist of a decay-heated oxidic region at the bottom and a metal layer on the top. The thermal loadings exerted on the vessel wall by the naturally circulating pool have been a subject of study for the last several years, see e.g. [1]. The primary interest has been the determination of the feasibility of the accident management scheme of retaining the melt within the lower head by cooling the vessel outside wall with water.

Much has been learned since the focused studies began. An evaluation of the in-vessel melt retention management scheme for the AP-600 was performed by Theofanous et al. [2], who found sufficient margin for the critical heat flux for heat removal at the vessel outer wall, over the thermal loading imposed by the circulating melt inside the lower head. The metal layer resident on top of the oxidic pool was found to focus the heat added to it from the oxidic pool towards the vessel wall. However, for the AP-600 geometry and scenario, it was found that the metal layer would be quite thick and the focused heat flux at the vessel wall was lower than the
critical heat flux at the vessel outer wall.

The AP-600 evaluation was based on data obtained from the COPO [3], UCLA [4] and the mini-ACOPO [2] experiments, employing water, freon and water, respectively as melt simulants. The mini-ACOPO experiments also employed the heat capacity of the melt simulant in a transient cool-down mode to obtain the heat transfer data. A reasonable equivalence between the volumetric heating and the transient cool-down has been demonstrated through CFD analyses [5].

Experiments and analyses have been continuing since the evaluation performed for the AP-600. The mini-ACOPO experiments have been transformed into ACOPO experiments, employing a one-half scale 3-D representation of the lower head; again employing water and the transient cool-down technique. The COPO facility has employed top and side wall cooling to create a crust (ice), i.e. a truly isothermal boundary [6]. The BALI experiments [6], conducted in Grenoble, have employed a full scale 110° slice facility, with water as melt simulant, also cooled at top and side wall to create an ice crust boundary.

The RASPLAV Program [7], conducted in Russia, employs prototypic (UO$_2$-ZrO$_2$) melt materials in a 200 kg slice facility, in which the thermal loadings imposed by the prototypic melt on a cooled vessel wall are measured. Two tests have been conducted so far, for which data is being analyzed. In addition, the RASPLAV Program has also employed a salt test facility in which experiments with eutectic and non-eutectic salt have been conducted. The data in some of the tests have been analyzed, while others need further analysis.

A separate-effect experiment [8] on the focusing effect of the metallic layer has been performed at the BALI facility. This experiment employed water as the metal-layer simulant. A focusing effect of $\simeq 2.0$ was found, which increased to $\simeq 6.0$ for very thin layers (aspect ratio of $\simeq 1/40$).

We believe the recent changes in the situation with respect to the prediction of the thermal loadings on the vessel wall, and with respect to the feasibility of the in-vessel melt retention may be described as follows:

- The RASPLAV experiments, conducted at $Ra' \leq 10^{11} - 10^{12}$, have shown corium melt stratification for prototypic compositions and temperatures. The interpretation of the data obtained with respect to stratification has not been completed so far. If the stratification is found to be stable, and prototypic, for the accident composition and temperatures, it may affect the natural circulation flow fields. The magnitude of the effects of stratification at the prototypic $Ra'$ numbers (when the flows would have greater turbulence than for those in the RASPLAV tests) has not been determined.

- The measured values of the $Nu_{up}$, $Nu_{dn}$ and $Nu_{sd}$, obtained from the recent isothermal-boundary COPO and BALI experiments, appear to be larger than those obtained from the ACOPO facility, which did not have crusts at the boundaries. These measured values are also larger than those derived from the Steinberner and Reineke correlation. The $Ra'$ number scaling, however, holds. These differences may be due to the changes in the boundary layer heat transfer at the crust boundary, or due to the change in the thermal expansion of water at +4°C. Resolution of these differences has not been achieved so far.

- The salt experiments performed in the RASPLAV Program have also shown some differences in the heat transfer at the boundaries of a naturally convecting pool with or without crust boundaries.
• The metal layer resident on top of a heat generating oxidic material pool was found to focus the heat, received by it, towards the cooled side wall of the vessel; thereby indicating that the vessel corner may be the most failure-prone location. This has been confirmed by separate-effect tests.

However, evaluations employing integral two-dimensional analyses of the oxidic pool, metal layer and vessel have indicated a significant amelioration of the peaking of heat flux at the vessel location next to a thin metal layer.

We believe that the recent changes have introduced uncertainties, which may not allow a straight-forward evaluation of the feasibility of in-vessel melt retention for a severe accident in reactors, with high power densities, in which thin metal layer could be formed and oxidic melt stratification could occur. Currently there are no integral experiments, employing either prototypic or simulant materials, modeling the prototypic integral situation of an oxidic pool and a metal layer, which can support such evaluation. We, therefore, believe that the SIMECO test program, described in the following paragraphs, employing:
- eutectic and non-eutectic binary salt mixture melts to represent crusts and crusts with mushy regions, respectively,
- turbulent natural convection flow fields,
- stratified melt configurations with different density salt mixtures,
- appropriate molten metals to represent metal layers,
- different boundary conditions,
- key parameter variations,
performed as step by step integral experiments will provide a valuable data base for the evaluation of the in-vessel melt retention as an accident management strategy for high power reactor of the EPR scale.

The specific objectives of the experiments in the SIMECO facility are to investigate (i) the effects of boundary crusts and mushy layers on natural convection heat transfer; (ii) the effects of melt stratification on natural circulation; (iii) the amelioration of melt stratification by turbulent flow fields, and finally (iv) the multidimensional heat transfer in, and between, the melt pool, the top metallic layer and the vessel. The data base obtained will supplement those obtained in the RASPLAV test facilities.

2 SIMECO facility and experimental program

Experimental simulation is performed in a slice-type facility which includes a semicircular section and a vertical section; Fig.1. Diameter, height and width of the test section are 530x620x90mm. Brass is used as the slice walls, except for the front wall. The vessel wall is represented by a 23-mm thick brass plate. The front wall is made either of special glass for flow and crust visualization, or, of a thin copper sheet for infrared thermostimulation. Two water cooling loops with controlled flow rates are used for cooling the vessel wall; Fig.2. Water temperature measurements are used to obtain the average heat flux data on the side walls. In addition, up to 36 K-type thermocouples are built in the brass vessel wall at different angles and locations. Measured temperatures are then used to derive local heat fluxes.

Binary salt mixtures are employed as melt simulant. Both eutectic mixture (50%-50%) and non-eutectic mixture (20%-80%) of NaNO₃-KNO₃ are used in the SIMECO experiments.
The binary-mixture phase diagram is quite similar to that of the binary-oxide core melt UO$_2$-ZrO$_2$. For the 20%-80% mixture the temperature difference between the liquidus and solidus is about 60K. Liquidus temperatures of the binary mixture are 220°C and 280°C for the 50%-50% and 20%-80% compositions, respectively. Previously, these salt mixtures were extensively, and successfully, employed as core melt simulants in melt-vessel interaction experiments performed at RIT/NPS [6]. The heat of fusion, heat capacity, density, viscosity, heat conductivity of these mixtures were measured to enable pre-test and post-test analyses of the experiments.

Metallic layers of different thickness can be located on top of the salt mixture pool. A thin and highly-conductive copper sheet is used to separate the two layers. Lead-bismuth alloy or cerrobend alloy are employed as the simulant fluid of the molten steel layer.

Several heat exchangers are employed to provide the upper boundary cooling condition. Control of the water flow and measurement of water temperature in the different heat exchangers enable determination of the local distribution of upward heat fluxes either on the upper surface of the melt pool in case without metal layer, or on the upper surface of the metal layer otherwise.

Inside the slice up to 36 K-type thermocouples are installed to measure the local temperature variations in the liquid pool and in the metallic layer. Emphasis is placed on the near-wall region to detect the crust existence. In total, up to 96 measuring channels can be simultaneously employed in the RIT/NPS data acquisition system using a HP-1300A mainframe.

Internal heating in the binary-salt melt pool is provided by thin wire-type heaters. Two heaters, 3-mm in diameter and 4-m long are uniformly distributed in the semicircular section. They can supply up to 4 kW of heating to the molten salt pool.

The SIMECO test matrix is designed to cover:
(i) different heat generation rates and different top and sidewall cooling conditions, and
(ii) variation of metallic layer thickness (thin, intermediate, and thick).
In a scoping test series, water was employed as melt simulant, while in the main test series, binary salt mixtures are employed. The SIMECO facility enables experiments with Rayleigh numbers up to $1.5 \times 10^{13}$ (with salt) or $3.2 \times 10^{13}$ (with water) for the pool natural convection and up to $10^8$ for the metallic layer natural convection. The flow fields are expected to be turbulent for these cases of $Ra$.

3 Experimental results and analysis

The SIMECO data base acquired so far is preliminary, since the experimental equipment operation and instrumentation is still being improved. The experiments performed so far include (i) mini-SIMECO experiments, using both eutectic and non-eutectic melts, (ii) SIMECO experiments, using water as working fluid, and (iii) the first SIMECO test series in which eutectic binary salt was employed as a melt simulant.

3.1 Mini-SIMECO test series

The mini-SIMECO test series was performed in a test facility similar to that described in the previous section, but smaller and much simpler. The main purpose of the mini-SIMECO program was to examine the technical problems for building the SIMECO facility with internal heaters. Nonetheless, the mini-SIMECO test series provided useful data on melt pool formation and heat transfer. In particular, visualization of the melt pool formation dynamics was obtained. The crust was found thickest at the lowermost region of the pool, while the top crust and the crust at the pool's corner were very thin. More importantly, analysis of the mini-SIMECO test results revealed, that for the longer-term transients of melt pool formation and heat transfer; the heating method used (i.e. internal heaters) provides good simulation of the volumetric heat...
source. This is valid because the time scale of convective heat transfer from the heater’s surface to a given control volume is much smaller than the time scales of either melt pool formation or transient heat up. Additionally, while the energy transfer rate is important, heat transfer processes in a particular control volume in the pool are indifferent to the way in which the heat was generated in other parts of the pool.

3.2 Simon water test series

The Simon water test series was conducted to test the performance of the water cooling circuit, heaters, thermocouples and DAS.

In this test series, truly isothermal boundary conditions were not provided on the pool boundaries. The vessel semicircular wall adapted itself to the heat fluxes. Nonetheless, very good mixing was observed in the pool’s upper region, with heat fluxes peaking at the pool corner. Figs.3-4 depict experimental results of the SW-4 test, in which 3.7 kW was delivered to the water pool by the wire heaters. The pool’s Rayleigh number reached $Ra' = 3.14 \times 10^{13}$. Upward and downward Nusselt numbers were estimated from measured data as $Nu_{up} = 476$ and $Nu_{down} = 209$. The upper Nusselt number is close to that determined from the Steinberner-Reineke correlation [12] and the new BALI correlation [6].

3.3 Simon eutectic-salt test series

Six experiments were conducted in this series. This section focuses on results and analysis of SSEU-3 test, in which a molten-salt pool was formed during the mixture heat up and re-melting processes. Approximately, 3.7 kW was supplied to provide $Ra' = 1.51 \times 10^{13}$. Results of the SSEU-3 test are presented in Figs.5-7. The transient heat up and melt pool formation occurred in about 2 hours. At the steady state the molten salt pool reached relatively high temperatures (more than 280°C, i.e. 60K superheat). Highest vessel temperature and heat fluxes were measured in the pool corner (at angles of 80°-90° degrees). Upward heat fluxes of 15.5 and 17 kW/m² were obtained from thermocouples and flow measurements in the two upper heat exchangers. Water temperatures were $\approx 8^°C$ at the inlet and $12^°C$ at the exit from heat exchangers. Measurements of the local upward heat flux distribution was attempted, but flow leak between the sections of the heat exchangers affected the results.

Modeling of the heat transfer processes in the Simon facility is performed by the MVITA code developed at RIT/NPS. MVITA models the melt heat-up and natural circulation phases with an energy equation in which equivalent conductivity and convective are introduced. MVITA assumes that the heat flow is carried to the pool upper surface by the fluctuating part of the upward turbulent velocity. This velocity in the prototypic scenario is very small, i.e. about 1-5mm/sec. The heat transport to the side wall is assumed to be across a boundary layer at the wall. The heat transfer in the boundary layer is given by an Eckert type correlation. The MVITA model has been verified against several measurements. It should be noted however that the data base on which the MVITA model was validated includes melt pool or fluid layer experiments with isothermal boundaries, but not frozen boundaries. Frozen boundaries were realized in COPO-II and BALI experiments. However, only in the Simon facility, processes in the vessel lower head are experimentally modeled in an integral fashion, i.e. including melt pool, crust, vessel wall and molten metal layer.
Calculated results for the SIMECO SSeu-3 test with the MVITA model, are shown in Figs. 5-8. It can be seen that the major trends in the vessel wall heat flux and temperature can be predicted by the MVITA model. The analyses performed indicated that a gap may have formed between the top heat exchanger and the salt pool, causing additional thermal resistance and modifying the top thermal boundary condition of the pool. Modifications of the SIMECO facility are being performed to ensure complete contact between the heat exchanger and the salt pool. Additional experiments in this test series will be performed to obtain and enlarge the data base.

4 Concluding remarks

Design and construction of the SIMECO facility has been completed and the first experimental series was performed. Turbulent natural circulation flow conditions are realized in the SIMECO tests, however the $Ra^*$ values achieved are much lower than those in the prototypic accident conditions and geometry. So far, 16 experiments were conducted: 10 with water and 6 with eutectic salt as melt simulants. A new experimental data base is being obtained.

In the 1998 year, other series of the SIMECO experiments will be conducted, with metallic layers with melt stratification and with non-eutectic salt mixture. Results of these experiments will be compared to the data reported here. After that the brass vessel will be replaced by a melt-able vessel to provide data pertinent to prototypical scenarios in which vessel melting is predicted to occur. The MVITA model includes description of vessel melting and will be validated with the data obtained.

We believe that the SIMECO facility will provide the integral data base which will delineate the effects on the vessel thermal loading of crust boundaries, of phase chang, of melt stratification and of the focusing of heat by the metallic layer.
Figure 3: SW-4 test: centerline temperature in the pool.

Figure 4: SW-4 test: heat fluxes in the vessel wall (determined from thermocouple readings).

Figure 5: MVITA calculational results versus the SSEu-3 test results for temperature in the vessel wall.

Figure 6: SSEu-3 test: transient temperature in the vessel wall.

Figure 7: MVITA calculated results versus the SSEu-3 test results for the vessel wall heat fluxes, determined from thermocouple readings.

Figure 8: MVITA prediction of the SIMECO SSEu-3 melt pool.
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References


Numerical Investigation of Turbulent Natural Convection Heat Transfer in an Internally-Heated Melt Pool and Metallic Layer


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Abstract. This paper presents results of numerical investigation of turbulent natural convection in an internally-heated oxidic pool; and in a metallic layer heated from below and cooled from top and sidewalls. Emphasis is placed upon applicability of the existing heat transfer correlations (obtained from simulant-material experiments) in assessments of a prototypic severe reactor accident.

The objectives of this study are (i) to improve the current understanding of the physics of unstably stratified flows, and (ii) to reduce uncertainties associated with modeling and assessment of natural convection heat transfer in the above configuration.

Prediction capabilities of different turbulence modeling approaches are first examined and discussed, based on extensive results of numerical investigations performed by present authors. Findings from numerical modeling of turbulent natural convection flow and heat transfer in melt pools and metallic layers are then described.

1 Introduction

In a hypothetical severe accident scenario in a LWR, core melt may relocate to, and accumulate in the reactor pressure vessel (RPV) lower plenum. Decay heating in the core melt may cause formation of a melt pool. In addition, latter relocations of metallic structure and/or separation of metallic, lighter, component from the oxidic debris may lead to formation of a metallic layer on top of the oxidic pool.

Natural convection in the decay-heated core melt pool and in the metallic layer have a profound impact on the thermal loadings of the lower head and the upper structures in the vessel. For reactor situations, the natural convection flow regimes are characterized by the very high Rayleigh numbers, due to both the large geometry scale, H, and the high heat generation rate $q_v$, $(Ra \sim H^3$ or $Ra' \sim H^5)$. For prototypic conditions, the $Ra' = 10^{15}-10^{17}$ and the flow regime is highly turbulent.

A number of experiments and analyses have been performed to study turbulent natural convection phenomena. On the experimental front, except for the RASPLAV test, the experiments have utilized simulant materials. It is difficult to realize experiments with the prototypic high Rayleigh numbers and prototypic boundary conditions. Similarly, it is difficult to model analytically turbulent pool behaviour at high Rayleigh numbers. Existing single-point closure turbulence models were found to be incapable of predicting flow and heat transfer in a situation which involves both stable stratification and unstable stratification regions (Dinh and Nourgaliev, 1997 [1], Nourgaliev and Dinh, 1996 [2]).

Numerical analyses of flows and heat transfer in this area remain, however, important, since they may help to achieve a better understanding of the role of different factors and to assure applicability and relevance of experimental data and correlations to prototypic reactor accident conditions. The objectives of numerical
modeling of turbulent flows and heat transfer in a core melt pool and a metallic layer are:
- to gain insight into physics of governing mechanisms;
- to examine effect of non-prototypical conditions in simulant-material experiments; and
- to develop a reliable prediction method, which can first be used to satisfactorily analyze the experiments performed so far, then, be applied to reactor-scale predictions.

The present study reports new results and reviews the progress in the field of turbulence modeling accomplished after the previous OECD/CSNI specialist meeting (Grenoble, 1993) on this topic.

2 Assessment of various turbulence models

2.1 $k - \epsilon$ turbulence models

A wide variety of models have been utilized to describe turbulent fluctuations in natural convection heat transfer. The available models can be divided into the following categories:
1. Algebraic model of turbulence
2. One-half equation model of turbulence
3. One-equation model of turbulence
4. Two-equation turbulence models
   (a) Standard $k - \epsilon$ model using wall functions (KEM)
   (b) Low-Reynolds-number $k - \epsilon$ model (LRN KEM)
   (c) Modification of LRN KEM for buoyancy-driven flows
5. Reynolds stress models (RSM)
   (a) Algebraic stress model (ASM)
   (b) Partial differential stress model (DSM)
6. Multiple-time-scale models
7. Large-eddy simulation (LES)

Besides the above models, direct numerical simulation (DNS) has also been employed to describe transient flow field during turbulent natural convection.

In a previous study, capabilities of different modeling methods were reviewed and discussed with respect to turbulent natural convection in a large volumetrically-heated liquid pool (Dinh and Nougaroliev, 1997 [1]). The KEM, perhaps the most widely used turbulence model, employs the eddy-diffusivity concept to model the Reynolds stresses and turbulent heat fluxes. This concept describes turbulence as a diffusion process characterized by a locally isotropic turbulent viscosity. A strict analogy between the Reynolds stresses and turbulent heat fluxes is assumed, since the turbulent Prandtl number $Pr_t$ is assumed as constant in the $k - \epsilon$ approach. It was found that at least a low-Reynolds-number $k - \epsilon$ model (LRN KEM) is required for modeling of flows in volumetrically-heated liquid pools. Moreover, modifications were necessary to account for the buoyancy-induced anisotropy of turbulence in such a liquid pool. Modifications based on the local Richardson number were employed (Dinh and Nougaroliev, 1997 [1]) for the turbulent Prandtl number and the near-wall viscosity, to account for the effects of density/temperature stratification on turbulence.

![Figure 1: Variation of the average upwards Nu numbers with Ra numbers. Calculational results vs. experimental correlation of Steinberger and Reineke.](image)

The above modifications employed in the $k - \epsilon$ model provided very good agreement with the
experimental data received from the Finnish COPO-I experiments [3] and the Steinberner-Reineke [4] tests (see Fig.1). Most importantly, local heat fluxes on vertical and curved pool boundaries were correctly predicted by the model. Nevertheless, these modifications of the low-Reynolds-number $k - \epsilon$ model are experiment-specific: they cannot substitute for the fundamental deficiencies of the two-equation turbulence model.

2.2 Reynolds-stress turbulence models

In order to account for the anisotropy of turbulence several approaches within the frame of Reynolds-stress modeling (RSM) have been developed for turbulent flow under gravitational influence. A comprehensive review of the RSM methods was provided by Dinh and Nourgaliev (1997) in ref.[1].

The decay phenomenon of a buoyant jet has been predicted from a differential $k - \epsilon - \theta^2$ turbulence model of Chen and Rodi (see Chen and Chen, 1979 [5]). In this approach, the turbulent stresses and heat fluxes are modeled by algebraic expressions while the differential equations are solved for the kinetic energy of turbulence ($k$), the dissipation rate of turbulence kinetic energy ($\epsilon$), and the fluctuating temperature ($\theta^2$). Furthermore, algebraic flux models also have been developed for turbulent buoyant jets (Hossain and Rodi, 1982 [6]) as well as natural convection in rectangular enclosures (Hanjalić and Vasić, 1993 [7]). The major conclusion in ref. [7] concerns the modeling of the turbulent heat flux vector, which was found to strongly influence the applicability of the model to a broader class of buoyant flows. Variants of the gradient diffusion model with isotropic and non-isotropic eddy diffusivity, and corresponding components of temperature gradients, were found to produce inconsistent results.

The turbulent natural-convection boundary layer for air flowing along a heated vertical plate, as measured by Tsuji and Nagano, 1988a [8], was investigated numerically with an algebraic (ASM) and fully differential Reynolds-stress model (DSM) by Peeters and Henkes (1992) in ref. [9]. It was shown that the turbulent Prandtl number ($Pr_t$) is certainly not constant over the whole flow field as was also seen in experiments by Tsuji and Nagano, 1988b [10]. Problems of wall modifications and constants, with respect to sensitivity of the ASM and DSM applied to natural convection flows, were analyzed and discussed broadly in the literature. On the one hand, the universality of a model increases with its complexity, but, on the other, the more complex are the models, the greater is the amount of empirical input needed in form of empirical constants, not to mention the increase in computing time. It is known that the ASM and DSM (especially the latter) are computationally expensive, and they are also numerically very unstable which can lead to serious convergence problems. Moreover, the wall damping functions for both of these types of Reynolds-stress models have to be validated even for simple flows and their turbulence empirical constants are not yet well-established.

Nourgaliev and Dinh (1996) [2] examined the validity of various assumptions proposed and utilized in different Reynolds-stress models for description of turbulent flow and heat transfer in liquid pools with internal energy sources. Physics of fluid in unstably-stratified flow region was studied by means of direct numerical
simulation (DNS) of naturally-convecting flow in internally-heated fluid layers, with a constant temperature boundary condition on the upper surface and an adiabatic boundary condition on the bottom surface. This approach enabled the determination of the top wall heat fluxes, the mean temperature fields, the distributions of Reynolds stresses and turbulent heat fluxes. The calculated turbulence parameters are analyzed with respect to Reynolds-stress type correlations. The calculated turbulent characteristics (Reynolds stresses and turbulent heat fluxes) indicate significant anisotropy of turbulent transport properties. So, the isotropic eddy diffusion approach cannot be used to describe turbulent natural convection heat transfer under unstable-stratification conditions.

![Figure 3: Dissipation time scale ratio $\tau$ (proposed in RSM as constant, typically, in the range from 0.4 to 0.8).](image)

Analysis of the thermal-variance-balance showed an important role of diffusive transport of $\overline{T^2}$, and remarkable non-equilibrium of thermal-variance $E_T$. Turbulence constants, needed for modeling of turbulent diffusion, and of dissipation of thermal-variance, are strong functions of Rayleigh and Prandtl numbers, and are non-uniformly distributed in the fluid layer; see e.g. Fig.2. Fluids with two different Prandtl numbers ($Pr = 7$ and $Pr = 0.6$) were investigated. Similar turbulence statistics of the thermal fields were obtained for different $Pr$ numbers, however, remarkably different results of turbulence statistics of the hydro-field were calculated; see Fig.3. As a consequence, important turbulence parameters and constants are found to be strongly dependent upon the fluid Prandtl number.

From the results of DNS of flows in internally-heated fluid layers, it was found that major correlations and assumptions proposed and employed in the previous Reynolds-stress modeling are violated. Thus, developing a higher order turbulence model for this type of flow, is not straightforward.

### 2.3 Large-eddy and direct numerical simulations

In order to produce a turbulence database, direct numerical simulation (DNS) can be employed. In this method, the full three-dimensional time-dependent conservation equations of mass, momentum, and energy are solved on grids which resolve the largest and the smallest scales of turbulence. The calculated time-dependent flow and temperature fields can, then, be analyzed for the fluctuations induced by turbulence. In general, such a calculation has to be performed with a very fine grid structure to adequately represent the small scale turbulence in the flow fields of interest. In the past, DNS has been used for analyzing natural convection heat transfer in fluid layers with internal heat generation with low values of $Ra$ numbers ($3 \cdot 10^4 \div 4 \cdot 10^6$) [11], Nourgaliyev and Dinh (1996) [2] performed direct numerical simulations in internally-heated fluid layers, using a finit-difference numerical scheme. A commercially available, general-purpose computer code CFX (FLOW3D) [12] was employed for calculations. Analyses were performed to evaluate grid resolution and time step requirements for the DNS calculations.

It was shown that three-dimensional formulation and proper description of mixing are mandatory in order to predict heat transfer in unstably-stratified flows. That is to say, natural convection heat transfer in these conditions is mainly governed by large eddies. Good agreement with heat transfer data was achieved.
and $\eta$ is the smallest length scale of fluid motion. Since this ratio is proportional to $Ra^{1/3}$, the total number of grid points required by a DNS calculation in three dimensions are $\sim Ra$. For this reason, it is extremely expensive and unaffordable to perform DNS of natural convection flows in the prototypic reactor range of Rayleigh number.

To overcome the problem of limitation in computational capacity, large eddy simulation (LES) has been employed to analyze heat and mass transfer in turbulent natural convection. In LES, large scale motion is simulated whereas motion with length scale smaller than the computational grid size is modeled by a subgrid scale model (SGM). Most subgrid scale models are based on the eddy diffusivity concept, introducing an effective eddy diffusivity and conductivity for the subgrid scales.

The original subgrid scale model of Smagorinsky (1963) [17] and Lilly (1967) [18] was based on dimensional arguments and integral relations coming directly from Kolmogorov's ideas on the turbulence energy spectrum. This model assumes the isotropy of the subgrid-scale turbulence. Such an assumption may not be valid for the near-walls regions.

The subgrid-scale eddy viscosity is defined as

$$\nu_t = (C_s \cdot h)^2 \cdot S \quad (1)$$

where $h$ is the grid size (mesh scale), $S$ is the scalar strain, and $C_s$ is the Smagorinsky constant. In the literature, the constant $C_s$ was found to vary in the range from 0.06 to 0.25. By assuming the existence of an inertial range spectrum, Lilly (1967) [18] evaluated $C_s \approx 0.18$.

The turbulent heat flux is often calculated by applying subgrid Prandtl number concept, $Pr_{SGS}$.

$$\alpha_t = \nu_t / Pr_{SGS} \quad (2)$$
Figure 6: Typical distribution of calculated subgrid-scale kinematic viscosity in an internally heated fluid layer ($\nu = 0.8610^{-6} \text{m}^2/\text{s}$).

The subgrid-scale Prandtl number may depend on the fluid properties. Results of LES simulation are however less sensitive to $Pr_{SGS}$ in fluids with the molecular Prandtl number close to one. Essentially, the model has two model constants $C_s$ and $Pr_{SGS}$.

Recently, a dynamic subgrid model was proposed and developed, in which the constant $C_s$ is not arbitrarily chosen or optimized, but computed. However, this model features numerical instability and convergence problems, and a robust numerical scheme has yet to be developed and validated.

The present authors employ the Smagorinsky-type model for LES in a large volumetrically-heated liquid pool and metallic layer. In such a pool, the core region is the major computational domain which requires huge number of computational nodes. For this region, a large eddy simulation is the most effective method. More importantly, it is found that heat transfer in large volumetrically-heated liquid pools is governed by large eddies, so that contributions from the sub-grid eddies in the mixing and heat transfer processes would remain minor, if the grid size is fine enough.

However, there appears a need in developing modification, both, for the constant, $C_s$, and for the subgrid Prandtl number, $Pr_{SGS}$ in the near-wall region. In order to avoid uncertainties associated with the near-wall treatment it is proposed that the near-wall computational mesh is fine enough to resolve the smallest scales of motion, i.e. to have a combined DNS in the near-wall region with LES in the pool’s core region.

Figure 7: Comparison between simulations with and without subgrid scale model ($C_s = 0.06, Pr_{SGS} = 1$)

Both DNS and LES approaches have been developed and employed at Royal Institute of Technology (Stockholm) to analyze effects of geometry configuration, heating method, and simulant properties on turbulent heat and mass transfer in a liquid pool and a fluid layer.
3 Concluding remarks

Results of recent experimental studies of natural convection heat transfer in an internally-heated liquid pool, and in a liquid layer heated from below provide a large data base for analyses. However, there still exist significant uncertainties when extrapolating (differing) heat transfer correlations (obtained in simulant material experiments, using different geometries, using different heating methods) to prototypic reactor accident conditions. Theoretical analyses and numerical modeling are therefore necessary to evaluate sufficiency and consistency of the current database.

Additionally, there exist other physical and chemical phenomena in a core melt pool which may affect turbulent flow fields and heat transfer. Notably, these are (i) behavior of crust at the pool's boundaries, (ii) phase change and stratification behavior of high temperature multi-component (at least, binary-oxidic) corium melts, and (iii) variation of melt physical properties with temperature and composition. These phenomena and effects should be investigated in conjunction with a reliable and generic turbulence model.

Although significant progress was made in the field of CFD applications and turbulence modeling in the past 4 years, capability and reliability of the current modeling approaches remain limited, particularly with respect to Rayleigh number conditions.

Nonetheless, it has been found that remarkable insights can be obtained from results of numerical investigations at lower Rayleigh numbers, for which the method of direct numerical simulation (DNS) is reliable and computationally affordable. Systematic examination of a particular effect for a wide Rayleigh number range (from $10^6$ to $10^{12}$) could provide a reliable extrapolation of the effect in reactor-scale Rayleigh numbers. This approach was extensively employed at Royal Institute of Technology (Stockholm) to perform many sensitivity and separate-effect studies.

For the purpose of reactor-scale calculations, a promising method is being developed at RIT. This method combines DNS at the wall region and large eddy simulation (LES) in the inside regions of the pool. Thus, the method has significant advantages over both the single-point closure models and the full-domain DNS approach.

References


Current Status and Validation of CONV2D&3D Code
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Nomenclature
\( c_{eff} \) - effective thermal capacity
\( C(v)v = \frac{1}{2}[(v, \nabla v) + \nabla (vv)] \)
\( \tilde{C}(v)T = \nabla (vT) \)
\( h \) - enthalpy
\( G^*(t) \) - initial domain
\( G(t) \) - regular domain, \( G^* \subset G \)
\( L \) - latent heat
\( N \) - the diffusive transport operator= div (\( \nu \nabla v \))
\( \tilde{N} \) - the diffusive transport operator= div (\( \kappa \nabla T \))
p - pressure normalized to the density
\( P \) - the gradient operator \( \nabla v = \nabla p \)
\( S^T \) - source for the energy equation
\( S^v \) - source for the momentum equation
\( T \) - temperature
\( T^* \) - melt temperature
\( v = (v_1, v_2, v_3) \) - velocity
\( x = (x_1, x_2, x_3) \) - coordinate

Greek
\( \delta \) - Delta function of Dirac
\( \kappa \) - thermal conductivity
\( \nu \) - kinematic viscosity
\( \Phi \) - solid fraction

1. Introduction. Analysis of Nuclear Power Plant (NPP) safety and accident management is associated with the concept of the "defence of depth" and is based on studying the protective properties of separate safety barriers and their stability to severe accidents. Two type of codes are used to analyze phenomena in course of severe accidents. Codes of the first type such as RELAP/SCDAP, ATHLET, CATHARE, etc. deal mainly with core thermal hydraulics and describe the processes up to early phase of core degradation. The second one consists of the codes which are based on simplified approaches (MELCOR, ESCADRE, etc.).

There are several reasons to use simplified approaches to modeling of severe accidents and one of them is that the uncertainties of severe accident analysis are relatively large. Usual approach is to develop models which later may be validated against large scale tests. Problems connected with the validation procedure are as follows:

- Severe accident codes for the reactor case can not be used adequately for simulation of experiments;
- Experimental conditions do not follow exactly severe accident conditions.

At the same time there is the necessity for more detailed analysis of the protective properties of barriers which can be used for the confinement of molten fuel. In particular, some approaches for next generations of NPPs use the concept of external coolability of vessel lower head due to flooding of reactor cavity. In this case the development of special software for detailed analysis of phenomena in course of severe accident becomes actual [24]. Such software is CONV2D&3D code intended for analysis of convection/diffusion processes with accounting for melting in a wide range of geometric parameters and boundary conditions for laminar, transitional and turbulent regimes.

In the present work a numerical technique is considered in order to solve diffusion/convection problem with regard of melting. Moreover, validation of CONV2D&3D code against existing of experimental results such as ACOPO and well-known correlations are given.

2 Numerical Technique
2.1 Problem Formulation. Unsteady Navier-Stokes equations in approximation of Boussinesq together with the energy equation are used for the description of diffusion/convection processes under the condition of melting in region \( G^* \):

\[
\frac{\partial v}{\partial t} + C(v)v - \nabla (\nu \nabla v) + \nabla p = S^v,
\]

\[
\text{div } v = 0,
\]

\[
\frac{\partial h}{\partial t} + \tilde{C}(v)h - \nabla (\kappa \nabla T) = S^T,
\]

where

\[
h = \int_0^T c_{eff}(\xi) d\xi,
\]

\[
c_{eff} = \begin{cases} 
    c + L\delta(T - T^*) & \text{for pure materials}, \\
    c + L\frac{d\Phi}{dT} & \text{for alloys}.
\end{cases}
\]

2.2 Operator-splitting difference schemes. The principle of splitting according to physical processes is basic during construction of a splitting scheme. For convenient presentation the split is done on the operators level.

We shall split the operators of the energy equation into two parts, which are associated respectively with the enthalpy and temperature. Calculation of a convective transport will be done on the first stage.

\[
\frac{h^{n+1} - h^n}{\tau} + \tilde{C}(v^n)h^{n+1/2} = 0,
\]
Diffusive part of the energy equation will be calculated on the second stage.

$$\frac{h^{n+1} - h^{n+1/2}}{\tau} - \mathcal{N} T^{n+1} = f^{n+1}.$$  \hspace{1cm} (5)

Here superscripts \(n\) and \(n+1\) are used to denote successive time levels of the time grid \(t^n = n\tau, n = 1, 2, \ldots\).

We shall split the operators of the momentum equation into two parts, which are associated with the velocity and pressure respectively. We use notation \(d/dt\) instead of \(\partial/\partial t\) since equation (1) has no formal form of a partial differential equation:

$$\frac{dv}{dt} + (A_1 + A_2) v = f, \quad t > 0,$$  \hspace{1cm} (6)

where \(A_1 = C(v) + \mathcal{N}, A_2 = P\). So, we can linearize this operator, using value \(v^n\) from the previous time level, i.e. everywhere below \(A_1 = C(v^n) + \mathcal{N}\).

We consider the additive scheme for the momentum equation that is similar to the well-known Douglas–Rachford scheme [1]. It was found from calculations that this scheme is certainly preferred over the other schemes, for example, Peaceman–Rachford type scheme.

$$\frac{v^{n+1/2} - v^n}{\tau} + A_1 v^{n+1/2} + A_2 v^n = f^n, \quad x \in \omega,$$  \hspace{1cm} (7)

$$\frac{v^{n+1} - v^n}{\tau} + A_1 v^{n+1/2} + A_2 v^{n+1} = f^n, \quad x \in \omega,$$  \hspace{1cm} (8)

$$\text{div}_x v^{n+1} = 0, \quad x \in \omega^*.$$  \hspace{1cm} (9)

Up to the point in this section we do not take into account particular form of the difference operators. Now we shall take it into consideration and discuss the numerical realization of the scheme (7)-(9). To implement the scheme (7)-(9), one can subtract the equation (7) from the (8) accounting that \(A_2 = \text{grad}_h\) and obtain so-called stabilizing correction equation:

$$v^{n+1} = v^{n+1/2} - \tau \cdot \text{grad}_h (p^{n+1} - p^n), \quad x \in \omega.$$  \hspace{1cm} (10)

Substitute it into the incompressibility constraint (9), taking into account that \(v^{n+1} = 0\) at the boundary \(\partial \omega\).

Denoting \(\Delta p = p^{n+1} - p^n\), we derive the Poisson equation to evaluate the pressure correction \(\delta p\):

$$\text{div}_x \text{grad}_h \Delta p = \frac{1}{\tau} \text{div}_x v^{n+1/2}, \quad x \in \omega.$$  \hspace{1cm} (11)

We consider also the additive scheme for the momentum equation that is similar to the locally one-dimensional scheme [2]. In this case equations (7)-(9) are written as

$$\frac{v^{n+1/2} - v^n}{\tau} + A_1 v^{n+1/2} = f^n, \quad x \in \omega,$$  \hspace{1cm} (12)

$$\frac{v^{n+1} - v^{n+1/2}}{\tau} + A_2 v^{n+1} = 0, \quad x \in \omega,$$  \hspace{1cm} (13)

$$\text{div}_x v^{n+1} = 0, \quad x \in \omega.$$  \hspace{1cm} (14)

The numerical implementation of the above scheme (12)-(14) is performed in the following way. Substitute equation (13) into the incompressibility constraint (14), taking into account that \(v^{n+1} = 0\) at the boundary \(\partial \omega\) and \(A_2 = \text{grad}_h\) we obtain the Poisson equation to evaluate the pressure correction \(p\):

$$\text{div}_x \text{grad}_h p^{n+1} = \frac{1}{\tau} \text{div}_x v^{n+1/2}, \quad x \in \omega.$$  \hspace{1cm} (15)

$$v^{n+1} = v^{n+1/2} - \tau \text{grad}_h p^{n+1}, \quad x \in \omega.$$  \hspace{1cm} (16)

2.3 Fictitious Domain Procedure. In solving computational fluid dynamics problems via finite difference methods in the primitive variables, there is a two variants of Fictitious Domain Methods (FDM): the first of them is based on the continuation of the coefficient at lower-order derivatives and the second approach uses the continuation of the coefficient at the highest-order derivatives [2]. Both approaches can be found in reviewed literature on numerical simulation of convection/diffusion phase change processes [3]. We employ the variant with continuation of equation coefficients at lower order derivatives, which in physical sense can be treated as incorporation into the initial Navier-Stokes equations a model of a porous medium.

Instead of equations (1), (2) in varying in time fluid domain \(G^*(t)\) we will solve in the whole problem domain \(G\) the following equations:

$$\frac{\partial v_z}{\partial t} + C(v_z) v_z = \text{grad}_p + \text{div}_x (\nu \text{grad}_x v_z) - c_r v_z + S^V,$$  \hspace{1cm} (17)

$$\text{div}_x v_z = 0.$$  \hspace{1cm} (18)

For the term described of hydraulics resistance in equation (17), the various formulae, based on the analogy of it with porous medium models may be apply. In so doing term \(c_r\) can be interpreted as the some resistance coefficient. Simple model corresponds to lack of smearing of the phase change interface, i.e. jump switching (following [4] "switch off") from liquid to solid fraction. In this case continuation coefficient \(c_r\) and the right hand side \(S^V\) are chosen like this:

$$c_r = \begin{cases} 0, & (x, y) \in G^*(t), \\ \varepsilon c_r, & (x, y) \in G/G^*(t), \end{cases}$$

$$S^V = \begin{cases} S^V, & (x, y) \in G^*(t), \\ 0, & (x, y) \in G/G^*(t), \end{cases}$$

where parameter \(\varepsilon\) prescribes estimate of error \(||v_z - v||\). Such approach with violent transition from solid to liquid in hydrodynamic equations is
yet applied for solving of Stefan problem (see, for example, Ref. [6]).

On this basis, the modified prediction-correction procedure like a Douglas-Rachford scheme can be written as

\[
\frac{v^n_{r+1/2} - v^n_{r}}{\tau} + A_1 v^{n+1/2} + A_2 v^n_{r} + c_\varepsilon (x) v^{n+1/2} = f^n_{r},
\]

(19)

\[
\text{div}_h \left( \frac{1}{1 + \tau c_\varepsilon} \text{grad}_h p^{n+1} \right) = \frac{1}{\tau} \text{div}_h v^{n+1/2}. 
\]

(20)

\[
v_{r+1}^{n+1} = v_{r+1/2}^{n+1} - \frac{\tau}{1 + \tau c_\varepsilon} \text{grad}_h \Delta p_r.
\]

(21)

\[
p^{n+1}_r = p^n_r + \Delta p_r.
\]

(22)

Application of FDM procedure to locally-dimensional scheme does to the following equations:

\[
\frac{v_{r+1/2}^{n+1} - v^n_{r}}{\tau} + A_1 v^{n+1/2} + c_\varepsilon v^{n+1/2} = f^n_{r},
\]

(23)

\[
\text{div}_h \left( \frac{1}{1 + \tau c_\varepsilon} \text{grad}_h p^{n+1} \right) = \frac{1}{\tau} \text{div}_h v^{n+1/2}.
\]

(24)

\[
v_{r+1}^{n+1} = v_{r+1/2}^{n+1} - \frac{\tau}{1 + \tau c_\varepsilon} \text{grad}_h p^{n+1}_r.
\]

(25)

3 Validation Aspect. Validation of numerical technique realized in CONV2Dx3D code was performed by means of a set of conventional “bench mark” problems. Among them were: the universally acknowledged test for solution of 2-D convection problems — natural convection in a square cavity with walls having different temperatures [7]; experiments on melting of pure gallium [8]; experiments on modeling convection of a heat-releasing fluid [9], including experiments at extremely high Rayleigh numbers, being accomplished at the mini-“ACOPO-B” facility.

3.1 Numerical Modeling of Convection/Diffusion Problems Accounting for Melting Processes. A standard test by which numerical procedures for calculation of phase transitions in pure materials are validated is the problem of gallium melting in a rectangular cavity [6]. In this paper, experimental profiles of the “liquid/solid” front at different instants of time are given. The specificity of the material used in the experiments should be noted: heat conductivity of solid polycrystalline gallium is anisotropic and may differ by as much as a factor of 5 along different directions. Nevertheless, is assumed that such uncertainty in solid phase properties is insignificant in the problem of melting (as distinct from that of crystalization). Therefore, this problem is used as a nonstationary test for 2-D calculations in Cartesian coordinates. Besides the above-mentioned publication [6], available is also a great deal of numerically calculated data obtained by both fixed grid techniques (see, for example, [10]) and algorithms with front isolation (for instance, [11]), including not only the interphase profiles, but also flow patterns with some local and integral characteristics.

The modeling of pure-gallium melting is performed under the formulation considered in numerical efforts [10] based on the fixed grid technique in case of identical thermal properties (heat capacity, heat conductivity and density) of solid and liquid phases. A general layout of test problem is presented in Fig.1.

The results are obtained under a dimensionless formulation and correspond to the Prandtl, Grashof and Stefan numbers equal respectively to \( Pr = 0.0216, Gr = 3.31 \times 10^7, Ste = 21.66. \)

A comparison of the numerical predictions with experimental findings [6] is accomplished using melting-front dynamics (see Fig.2). The figure suggests that the numerical predictions provide a sufficiently good agreement with the experiment despite some uncertainties in the latter.

Among the uncertainties of the experiment [6] affecting, in our opinion, the accuracy of the obtained pattern of the distribution of phase-transition boundaries (see Fig.2), the following should be noted:

1. the phase-transition boundary is determined by means of a relatively small number of thermocouples (13 at the upper and lower horizontal walls and 17 in the middle cavity section), i.e., the front profile given in [6] is based on three points. This results in a relatively arbitrary interpretation of intermediate values, which is particularly pronounced in the intersecting profiles corresponding to time moments 12\(1/2\), 15 and 17 minutes.

2. In numerical experiments of different researchers, interpretation of an experimental study slightly varies in what concerns the initial and boundary problem. To be more precise, in [11] the initial temperature of the solid phase and the temperature of the right-hand vertical wall corresponded to that of the phase transition whereas in calculations [10] these initial and boundary conditions were by 1.5 degrees below the melting point.

It should be noted that the calculations were performed on a sufficiently detailed, uniform grid 140 x 50 steps. Taking the foregoing into account, juxtaposition of predictions and experimental data, allow to speak about a satisfactory agreement. In detail see Ref. [8].

3.2 Convection at high Rayleigh number

3.2.1 Approximating turbulence model. The analysis of the natural convection phenomenon in the molten pool shows [12] that the flow pattern and, as a consequence, the heat transfer inside
corium depend on the convection regime, which is characterized by the Rayleigh number and cooling regimes on the external boundaries. Considering convection at high Rayleigh numbers, the turbulence of flows should be accounted. In this connection the problem of usage of adequate turbulence models for simulation of natural convection is actual. The turbulence models can be classified in several ways. The one most often used is that arranged in order of the number of differential equations solved in addition to the mean flow equations: (I) zero equation models; (II) one equation models; (III) two equation models; (IV) stress equation models. Most of the models, classes (I)-(III), use Boussinesq eddy viscosity model. Other models which do not use the eddy viscosity assumption (class IV) obtain the Reynolds stress from differential equation. Zero equation model, which uses only the partial differential equation for the mean flow field and no transport equations for turbulence quantities, is also called ‘mean field’ closure. The classes (II) to (IV) are called ‘transport equation’ closures. An extensive review of the various models is provided by Nallasamy [13].

The results of the recent papers (see, for example [12]) showed, that there is no common approach to the solution of adequate model choice problem. So, the flux-based Reynolds stress model requires solution of many more transport equations (for turbulent fluxes) in comparison to the two-equation models and hence is computationally more expensive than the model. The standard version of the two-equation model (high-Reynolds number version) is applicable only for fully turbulent flows. Therefore, even if the effect of buoyancy on turbulence is included, this model cannot predict the possible simultaneous presence of laminar and turbulent flow regions in the pool. Secondly, the standard model is known to overpredict heat transfer at the walls [12]. On the other hand, the low-Reynolds number version of the two-equation model is applicable over turbulent as well as laminar flow regimes. Also, in this model, the calculations are carried out up to the wall so that accurate prediction of wall heat transfer is possible. At present, numerical investigations of natural convection of a heat-generating fluid (for example, [12]) are usually based on low-Reynolds turbulent models Ref. [14]. At the same time the usage of low-Re turbulent model may lead to some discrepancy of numerical predictions against experimental data on natural convection in a volumetrically heated fluid with high Ra number [15]. This discrepancy may be explained in a way that the empirical coefficients in turbulent model were obtained for fluid flows, which significantly differ from buoyancy driven volumetrically heated flows.

In calculations turbulence models depend on limited number of parameters. Among this family of algebraic models approach is commonly used. A comprehensive review of algebraic models is presented in Reference [16].

In the course of numerical studies carried out especially for the problem under consideration, in order to account for turbulent nature of the flow, typical of fluid convection at Rayleigh numbers above \(10^7\), an approximating model of turbulent viscosity was fitted in the form as follows:

\[
\nu_T = \nu_0 \left(1 + \frac{\text{Re}_{loc}}{\text{Re}_{crit}} f(Ra)\right),
\]

where \(\text{Re}_{loc}\) is the local Reynolds number; \(\text{Re}_{crit}\) is the critical Reynolds number equal to \(10^4\); \(f(Ra)\) is a Rayleigh number function calibrated by numerical calculations of natural convection in a cavity with walls of different temperature, \(f(Ra) \approx Ra^{1/5}/100\).

The predictions by the CONV2D code using the above model (they are denoted with markers “O” in Fig.3) allowed to attain full coincidence with the correlations for the class of flows under consideration as well as to surmise adequacy of the above approach for other flow types (for example, see Fig.4).

Nonetheless, one should not forget that, as the presented approximation model of turbulent fluid was selected for flows lowering or upgoing along vertical walls for the problem of convection in a cavity of walls of different temperature and a Rayleigh number range \(\leq 10^{13}\), the use of the model when modeling flows at higher Rayleigh numbers in an unstable vertical liquid layer may yield no appreciable improvement of results. The last circumstance, in its turn, may require refinement or even development of a separate model for flows markedly unstable in the vicinity of the upper boundary, which is planned to be implemented in the nearest future.

3.2.2 Convection in a Square Cavity with Walls at Different Temperatures. A standard test by which numerical procedures for calculation of natural convection are validated is 2D natural convection problem in a square cavity with walls having the different temperatures.

The numerical predictions are given then is performed using the fixed grid technique with natural variables under the temperature formulation.

The result are obtained with help of CONV2D code under a dimensionless formulation and correspond to the Rayleigh (Ra) and Prandtl (Pr) numbers equal, respectively \(10^5 \leq Ra \leq 10^{12}\), \(Pr = 0.071\).

A quantitative juxtaposition of the results of numerical modeling of convective flow with bench mark results Davis [19] in the range of Rayleigh number up to \(10^6\) is achieved. In the above-mentioned range of Rayleigh numbers a quantitative juxtaposition of predictions with correlations Jacob [20] is obtained (see Fig. 3).
3.2.3 Benard Convection in a Square Cavity with Walls at Different Temperatures. A 2D problem on convection in a square cavity is considered for the upper and lower walls kept at specified constant temperatures and the side ones with full heat insulation.

Numerical modeling is performed under a dimensionless formulation with the values of dimensionless temperature at the upper and lower boundaries equal to 0 and 1 respectively.

The predictions are based on the CONV2D code and derived using the approximating model of turbulence.

A quantitative juxtaposition of the results of numerical modeling of convective flow and well-known correlation relationships [21] is obtained in the range of Rayleigh numbers from $10^5$ to $10^{10}$ under Prandtl number equals to 0.7 (see Fig.4).

3.2.4 Mini-ACOPO-B experiment. The mini-ACOPO B facility was built for operation with water at temperatures up to 100°C to pursue a special investigation on Prandtl number effects [17]. In this apparatus, the two shells were made of copper one inside the other, with a spacing coil in between, to create the flow channel necessary for cooling down to a uniform temperature. The top was built from two copper plates built in a similar fashion. Experiments were performed in a semisphere that was closed with a flanged connection and adequate insulation in between to ensure that the two cooling circuits could operate completely isolated from each other.

Three runs were carried out in the mini-ACOPO B with water as the working fluid to explore some Prandtl number effects at initial water temperatures of ~ 100°C and cooling circuits operated to maintain wall temperatures of 3, 25 or 65°C. These experiments could cover the ranges of Prandtl number 2.5 < Pr < 11 and modified Rayleigh number $10^{12} < Ra' < 3 \cdot 10^{13}$ (Ra' = Gr Pr Da). The multipliers (Grashof, Prandtl, Dammkohler numbers) in the expression for the modified Rayleigh number were defined by the relationships given below:

$$Gr = \frac{g\beta(T_{max} - T_i)H^3}{\nu^2}, Pr = \frac{\nu}{\alpha}, Da = \frac{Q H^2}{\kappa(T_{max} - T_i)}$$

$Q$ is the rate of volumetric heat release calculated as $Q = (S_{up}q_{up} + S_{dn}q_{dn})/V$, where $q_{up}, q_{dn}$ are flux densities at the upper and lower boundaries, $S_{up}, S_{dn}$ are the areas of the upper and lower boundaries, $V$ is the volume. The rest parameters used in the above dimensionless expressions, $H, \beta, \kappa, \nu, \alpha$, denote respectively the characteristic region dimension in vertical direction, coefficient of thermal expansion of the fluid, coefficient of heat conductivity, kinematic viscosity, and coefficient of temperature conductivity.

In the course of the experiment, the major attention was paid to the pattern of distribution of heat fluxes onto the lower boundary of the cavity. The heat transfer was characterized by the Nusselt number defined as follows:

$$Nu = \frac{qH}{\kappa(T_{max} - T_i)}$$

where $q$ is the average heat flux through the boundary under consideration at a temperature of $T_i$, while $T_{max}$ is the maximum temperature in the fluid. The evidence obtained in the course of the experiments was generalized in the form of correlation relationships for the Nusselt number distribution at the lower boundary as a function of the modified Rayleigh number:

$$Nu_{dn} = 0.048 Ra'^{0.27} \text{ for } 10^{12} < Ra' < 3 \times 10^{13}$$

$$Nu_{dn} = 0.0038 Ra'^{0.35} \text{ for } 3 \times 10^{13} < Ra' < 7 \times 10^{14}$$

Below, some results are given on numerical modeling of the ACOPO experiment with the isothermal boundary conditions at the test wall equal to 3°C. The results were obtained with the help of the CONV2D code.

In Fig.7, displayed are the results of a quantitative comparison predictions with the experimental correlations derived from the evidence in [17]. The observed coincidence of the values suggests a sufficiently good modeling of the experiment conditions with the help of the CONV2D code.

3.2.5 Numerical Modeling of Convection of a Heat-Releasing Fluid. The actuality of this problem is conditioned by the need in correct predictions on behavior of a melted heat-releasing fuel in cases of severe accidents at nuclear power plants (PWR). When the core melts and the molten fuel flows down to the reactor vessel bottom, free convective flows due to heat release arise. A similar scenario was observed in the case of the well-known accident at the “Three Mile Island” plant, USA, in 1979.

In this section, some results on modeling of convective heat exchange in a layer of heat-releasing fluid are presented that are analyzed from the viewpoint of the problem of molten-fuel confinement within the reactor vessel or shaft. This problem has a lot of formulations that differ both in geometry of the region under investigation (cylindrical, semispherical, horizontal layer, etc.) and formulation of boundary conditions (uniform or adiabatic temperature conditions). A detailed review of different formulations and the results obtained for them are presented in the paper by Kelkar et al. [12].

The findings of Mayinger [18] obtained in the course of an extended experimental and theoretical
program accomplished in 1970–1982 at the Technical University of Hannover were used as the base for validation of the computational procedure suggested. The principal objective of the program consisted in studying the behavior of melted zones and peculiarities of heat transfer through upper and lower boundaries. The studies were fulfilled with water for different types of test cell geometry and different methods employed for simulation of internal heat release. In the course of these investigations, a number of experimental correlations were obtained that are used nowadays for validation of computational algorithms and codes under development.

Mayinger et al. performed a series of experiments [18] with water for a rectangular cavity in a horizontal slit at different water layer heights (the height varied in the range 5 to 160 mm and the width, 20 to 60 mm). For this experimental series, the same isothermal conditions were specified at the upper and lower surfaces of the cavity while the other boundaries were adiabatic. The aspect ratio in the experiment varied in a range 0.05 < H/D < 0.43 and the Rayleigh number, in a range $4 \times 10^4 < Ra' < 5 \times 10^{10}$. As a result of the experiments accomplished, the following correlation relationships were derived for Nusselt numbers at the upper and lower surfaces within the above range of Rayleigh numbers:

$$\text{Nu}_u = 0.345 \times Ra'^{0.333}, \quad \text{Nu}_d = 1.389 \times Ra'^{0.095}.$$  

Next, a series of experiments on the heat and mass transfer was performed in a specially designed semicircular experimental cell [18]. A distinguishing feature of these experiments consisted in investigation of testing cells with different radii, namely, $R = 2.5, 5.0, 7.5, 9.0, 12.8, 28.0$ cm. When processing the findings, a modified Rayleigh number was employed as the determining dimensionless parameter:

$$Ra' = \frac{\alpha H^5}{\gamma \nu}.$$  

For ranges of dimensionless parameters $0.05 < H/D < 0.5$ and $10^5 < Ra' < 5 \times 10^{10}$, the following dependencies were derived:

$$\text{Nu}_u = 0.36 \times Ra'^{0.23}, \quad \text{Nu}_d = 0.54 \times Ra'^{0.18}.$$  

The 2D calculation results presented below are based on the CONV2D code and the approximating model of turbulence within a Rayleigh number range $10^7 \leq Ra \leq 10^{14}$. In Fig. 8 displayed are the temperature fields for Rayleigh number $10^{12}$, which are qualitatively similar to the well-known findings of Mayinger [18]. A quantitative juxtaposition of CONV2D-based predictions used distributions of heat fluxes onto cooled cavity boundaries. For example, the Nusselt number values calculated using the CONV2D code are presented in Fig. 9 as compared to the correlation relationships [18]. Positions of markers in the figure feature close coincidence with the correlations for heat fluxes through the upper and side surfaces. A slight discrepancy observed at Rayleigh numbers above $10^{12}$ for heat fluxes at the lower boundary can be referred to an insufficiently detailed grid in the vicinity of the boundary.

Apart from the distribution of heat fluxes onto cooled cavity boundaries, the time required for reaching a quasi-steady state is estimated. In Fig. 9, the time of reaching a quasi-steady state is given. It is expressed by means of a dimensionless Fourier number and compared to the available dependencies [22]. These estimates feature a very good agreement with the correlations.

Presented are some results of CONV3D code validation using available experimental evidence for the problem of convection of a heat-releasing fluid in a semicircular cavity with cooled walls. Numerical results are compared to the experimental evidence obtained by Mayinger et al. [18]. Compared are also Nusselt number distributions at the lower boundary of the semicircular cavity. Here the comparison is presented at the maximum Rayleigh number value out of those given in [18] ($Ra' = 10^{15}$), which corresponded to the experiment in a cavity with the radius $R = 128$ mm.

Fig. 10 shows 3D temperature field as well as the flow distribution as a function of the angle $\phi$ as compared to the experimental evidence of [18]. The observed coincidence of the values suggests a sufficiently good modeling of the experiment conditions with the help of the CONV3D code.

### 3.2.6 Numerical Modeling of Salt Experiments

Within the RASPLAV project a few salt experiments were performed which allowed to determine experimentally the differences in heat transfer caused by crust formation near the test-wall [23]. These experiments differed in deposited power and temperature difference values at the test wall surface as well as in initial temperature materials and heating ways.

The results were obtained with help of CONV3D code and demonstrated a quantitative coincidence with experimental data (see Fig. 11).

#### 4 Conclusions

The modified scheme (23)–(25) is less precise but more robust in comparison to scheme (19)–(22). In solving of problems with moving boundary this scheme has a more weak limitations on integration step $\tau$, because stabilizing correction equation (25) is solved for the pressure $p$ instead of difference $\Delta p$. In this case limitations on integration step $\tau$ are caused by necessary con-
ditions of accuracy for considered problem.

Validation of CONV2D&3D code was performed by means of a set of experimental and conventional benchmark data (see Tab. 1). The results obtained with help of CONV2D&3D code demonstrate a quantitative and a qualitative coincidence both the experimental data and the well-known correlations.

| Table 1:  |
| --- | --- |
| **with melting** |  |
| stationary | Salt experiments [23] |
| non-stationary | Gallium experiments [6] |
| **without melting** |  |
| stationary | Convection in a cavity with hot & cold sidewalls [10] |
| Benard convection [21] |
| Convection of a heat-generating fluid [18] | Salt experiments [23] |
| non-stationary | ACOP experiments [17] |

References
5. P.N. Vabishchevich, O.P. Iliev, Numerical solution of conjugate heat and mass transfer problems including phase change, Differential'nye Ura
Fig. 1: General layout of test problem

Fig. 2: Melt-front dynamics

Experiment

Calculation
Fig. 3: Convection of fluid between walls under different temperatures

\[ \text{Nu} = 0.082 \text{Ra}^{0.329} \]

- CONV2D-turbulence model
- CONV2D+turbulence model

Fig. 4: Benard Convection

\[ \text{Nu} = 0.123 \text{Ra}^{0.294} \]

- CONV2D+turbulence model

Fig. 5: Convection of fluid between walls under different temperatures. Temperature field. \( \text{Ra} = 10^6 \)
Fig. 6: Benard convection. Temperature field. $Ra=10^6$

Fig. 7: mini ACOPO-B experiment

Fig. 8: Convection of a heat-generating fluid. 2D Temperature field. $Ra=10^{12}$
Fig. 9: 2D Convection of a heat-generating fluid

$$\text{Nu}_{up} = 0.345 \text{Ra}^{0.332}, \quad \text{Nu}_{ud} = 0.6 \text{Ra}^{0.19}$$

$$\text{Nu}_{do} = 1.359 \text{Ra}^{0.095}, \quad \text{Nu}_{dud} = 0.85 \text{Ra}^{0.19}$$

CONV2D + turbulence model

Fig. 10: Mayinger experiment.
3D convection of a heat-generating fluid
Fig. 10: /continue/

Fig. 11: Comparison of different ways of heating
FREE CONVECTION OF HEAT-GENERATING FLUID IN A CONSTRAINED VOLUME AND
THE CONDITIONS GOVERNING THE CHARACTERISTICS OF SHAPE DURING
EXPERIMENTAL SIMULATION OF HEAT TRANSFER IN SLICE GEOMETRY

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ABSTRACT A comparison of the structure of heat- and mass transfer in a heat-generating fluid
enclosed in volumes of different geometry is employed to theoretically analyze the correspondence (as
regards heat transfer) between a three-dimensional volume and a quasi-two-dimensional analog employed in
simulation experiments, as represented by a thin vertical slice (slice volume). It is found that the maximum
possible correspondence consists in that the exponents in the power dependencies of the Nusselt number on
the modified Rayleigh number \( Ra_f \) coincide, while the coefficients of these dependencies differ from one
another. The condition of such correspondence consists in the smallness of the thickness of boundary layers
of three types, developing in the slice volume, as compared with the thickness \( L \) of the latter. This condition
reduces to the inequality \( L/R >> Ra_f^{-1/6} \), where \( R \) is the characteristic space scale of the prototype 3D-
volume.

1. Introduction
Within the framework of investigations aimed at development of a safe nuclear reactor,
the problem arises associated with the processes of convective heat transfer of a heat-generating
fluid contained in a closed volume. The experimental simulation of these processes meets
with a number of difficulties, of which the main one resides in maintaining uniform bulk heat
release. One way of overcoming this difficulty consists in replacing the real three-dimensional
volume, in which the fluid is enclosed, by its thin central vertical slice \([1, 2]\). Such a formulation of
the experiment gives rise to the question of the conditions of correspondence between the
processes of heat transfer in real three-dimensional geometry (prototype volume) and in
quasi-two-dimensional (slice) geometry.
This study is devoted to theoretical analysis of the above-identified conditions for a one-
component heat-generating fluid. This analysis will be based on the results of comparison of the
structure of convective heat- and mass transfer in volumes that are three-dimensional (3D-) and
quasi-two-dimensional (quasi-2D-) objects. In the subsequent section, we will characterize the
maximum possible correspondence of heat transfer in the prototype and simulating volumes.
In Section 3, we will find the condition of such correspondence.

2. Correspondence of heat transfer between
3D- and slice volume
We will assume that a prototype 3D-volume taken up by fluid corresponds to a body of
revolution around a vertical axis with the horizontal upper portion of the boundary \( S_{up} \) and
finite radius of its curvature \( R \) at the lowest point (pole). We will further assume that the quantity \( R \)
is, at the same time, the characteristic size of the entire 3D-volume being treated. The central
vertical cross-section of this volume is given in Fig.1.
The position of the running point in the downward-directed curvilinear portion of the boundary $S_{dn}$ will be characterized by the polar angle $\theta$ counted from the vertical axis, with the value at the pole of $\theta=0$. We will designate the vertical coordinate, counted from the pole level, as $z$. For definiteness, we will further dwell on two options of the boundary conditions, namely, 1) a fully isothermal boundary, and 2) an isothermal, downward-directed curvilinear portion of the boundary $S_{dn}$ and an adiabatic upper plane horizontal portion $S_{up}$. The temperature will be counted from the temperature of the cooled portion of the boundary.

![Fig. 1. Central vertical cross-section of the initial 3D-volume](image)

![Fig. 2. Simulating slice volume.](image)

The volume of fluid in the simulation experiment corresponds to the central vertical slice of the prototype body (see Fig. 2) with heat-insulated vertical flat walls and the thickness $L$ that is much less than the characteristic space dimension $R$ of the slice plane,

\[ L << R \]  

(1)

By virtue of inequality (1) the simulating volume is approximately a transverse slice of a long horizontal cylinder of the same cross-section. We will make use of this fact in the course of further analysis and treat the cylinder as an intermediate object between the initial 3D-volume and its slice analog. The characteristics of correspondence of heat transfer between volumes of different geometries may be derived from proper understanding of the mechanisms of convective heat transfer. According to modern physical concepts, the general pattern of heat transfer of a one-component heat-generating fluid confined in a closed 3D-volume may be described as follows [3, 4]. For a fully isothermal boundary (it is this option of the boundary conditions that is implied in Fig. 1), a horizontal plane, which passes through the maximum of the average value of fluid temperature in the volume $T_{max}$ divides the entire volume into two parts that are comparable in magnitude but different as regards the behavior of heat- and mass transfer. An inverse distribution of temperature develops in the $V_+$ region above this plane and, therefore, the structure of flow and heat transfer in this region is close to Rayleigh-Benard convection [5], this defining the characteristics of heat transfer to the upper horizontal portion of the boundary $S_{up}$. Under conditions of developed convection, the temperature distribution in the bulk of the $V_+$ region (outside of the boundary layers), formed by large-scale convective flows, is, on the average, close to uniform. The temperature drop from the volume to the boundary region $S_{up}$ and, accordingly, the principal part of thermal resistance fall on the narrow surface layer in which the effect of volume heat sources on the heat flux to the boundary is insignificant. By virtue of this, the Nusselt number value that is average over the upper portion of the boundary $S_{up}$, expressed in terms of the unmodified Rayleigh number,

\[ \overline{Nu}_{up} = \overline{Nu}_{up}(Ra) \], reduces to the respective value for Rayleigh-Benard convection.

The heat transfer to the lower curvilinear portion of the boundary $S_{dn}$ is determined by the characteristics of the boundary layer being formed in this region, in which the fluid being cooled flows downward, while the properties of this boundary layer are largely close to those of an ordinary free-convection boundary layer in the vicinity of a vertical wall. It is therefore that the average value of the Nusselt number in the $S_{dn}$
region, as a function of the Rayleigh number \( \frac{Nu_{dn}(Ra)}{N} \), reduces to the value of the Nusselt number for a vertical free-convection boundary layer with ambient isothermal medium, within the numerical factor dependent on the shape of the fluid-containing volume.

The behavior of the boundary layer in the heat-generating fluid varies considerably as it moves downward and approaches the pole. Here it becomes convergent, the flow in the layer decelerates, it broadens and returns the fluid to the bulk of the volume, where a slow return flow is formed accompanied by a stable temperature stratification of the fluid in the \( V_r \) region (see Fig. 1). As a result of all this, at \( \theta \to 0 \), the heat flux to the boundary and, accordingly, the Nusselt number experience a sharp decrease and reach a minimum at the pole at \( \theta=0 \) [6]. The distribution of the heat flux in the neighborhood of the pole is of special interest from the standpoint of the problem of external cooling of the reactor vessel, in view of the difficulties involved in removing heat from this region of the boundary [7].

The above-identified qualitative characteristics of heat transfer to the boundary region \( S_{dn} \) hold for the boundary conditions of the second type as well, when this region is isothermal and the \( S_{up} \) region is heat-insulated. In this case, the \( V_r \) region is absent, and stable stratification takes up the entire bulk (containing no boundary layer) of the volume. Accordingly, the decrease of the heat flux during downward motion along the boundary is now more pronounced.

We will now discuss the qualitative characteristics of the behavior of heat transfer for a long horizontal cylinder. In the \( V_r \) region, for the boundary conditions of the first type, convective flows in the initial volume and in a long horizontal cylinder differ from one another, however, under conditions of developed convection, they provide for an almost uniform distribution of temperature in the given part of the volume in the case of cylinder as well. As a result of this property, the average values of the Nusselt number in the \( S_{up} \) region of the boundary as a function of unmodified Rayleigh number in two versions of 3D-volume coincide. As to the

heat transfer in the downward direction, a difference is observed between them, which reflects on the temperature distribution and on the flow in both the boundary layer and the bulk of the \( V_r \) region. These characteristics in both the initial and cylindrical volumes are two-dimensional, but they are axisymmetric in the former case and plane in the latter case. Nevertheless, the boundary layer in the cylindrical volume in the \( S_{dn} \) region of the boundary retains the qualitative similarity with the free-convection layer on the vertical wall. Therefore, the dissimilarities in the shape of two 3D-objects, have no effect on the behavior of the dependences of the Nusselt number on the Rayleigh number, as well as on the polar angle \( \theta \) at \( \theta \to 0 \), and affect only the coefficients (being function of \( \theta \)) in these dependencies. The reason for such feature is in that the large-scale flow and temperature distributions, being different for two types of volume, play only role of external conditions for boundary layers which, as to the local structure, remain the same for the two cases. It should be noted, that the shape of the volume may also govern the boundary for transition between the laminar and turbulent modes of the boundary layer (and, consequently, between two types of the dependence of the number \( Nu \) on the number \( Ra \)), as well as the place at which \( Nu \) reaches the limiting dependence at \( \theta \to 0 \) that corresponds to a sharp decrease in the heat flux. Naturally, after conversion of the dependence of the Nusselt number from the ordinary Rayleigh number to a modified one, \( Ra_f \), the difference between coefficients in the power dependencies for different types of 3D-geometry will reveal itself with respect to \( Nu_{up} \) as well.

The distinguishing feature of convective flow in a volume of slice geometry as compared with the prototype volume and its cylindrical analog consists in the restriction of flow on a normal to the slice and in the presence of viscous friction against the vertical heat-insulated walls. The unfavorable (from the standpoint of adequacy of simulation of heat transfer) effect of these two factors may occur both by way of direct distortion of the structure of boundary layers in the cooled regions of the boundary and due to modification of the pattern of heat- and mass transfer in the \( V_r \) and \( V_c \) regions; in its turn,
this modification is capable of affecting the structure of boundary layers as well. In the case, where the effect of these factors, with inequality (1) being valid, can be rendered insignificant, the characteristics of heat transfer in slice geometry will be close to those for a long horizontal cylinder, because the time-averaged pattern of flow and temperature distribution in both cases will be two-dimensional plane one, while in the prototype 3D-volume this pattern is, as already noted, axisymmetric. Therefore, the maximum possible correspondence (as regards heat transfer) of the simulating slice volume to the prototype 3D-volume reduces to the correspondence of a long horizontal cylinder to the 3D-volume.

The minimum space scale for the flow velocity and temperature in boundary layers is defined by the thickness of the latter. Therefore, the necessary condition for maintaining the structure of boundary layers in the cooled regions of the slice-volume boundary as compared with a long horizontal cylinder is the smallness of the maximum one of the thicknesses of these layers as compared with the slice thickness,

$$\delta_{\text{max}} \ll L$$

The convective flow and temperature distribution over the slice-volume thickness in the bulk of the $V_+$ region may be assumed sufficiently uniform provided the shear layer thickness for the large-scale part of flow on the vertical heat-insulated regions of the boundary is small compared with the thickness $L$, that is, meets the same requirement as the boundary layer on cooled walls (2). In this case, large-scale flow is capable, as in the 3D-volume, of providing an almost uniform distribution of temperature in the bulk of the $V_+$ region.

Yet another condition of adequacy of simulation of heat transfer in slice geometry consists in the requirement of maintaining the structure of stable temperature stratification in quasi-two-dimensional geometry, which is equivalent to the condition of smallness of the thickness of temperature boundary layers on vertical heat-insulated walls in the $V_+$ region.

Therefore, the general condition of equivalence (as regards the distribution of heat transfer) of the slice volume to a long horizontal cylinder and, consequently, of its maximum possible correspondence to the prototype 3D-volume consists in the requirement of smallness, as compared with the slice thickness (2), for the thickness of boundary layers of three types, namely, those on cooled walls, shear layers for large-scale flows on heat-insulated walls in the $V_+$ region, and temperature boundary layers on the same walls in the $V_+$ region.

The subsequent section is devoted to the analysis of condition (2).

3. Condition of correspondence of heat transfer in the simulating and prototype volumes

According to [6], the maximum thickness of the boundary layer in the cooled region of the boundary, $\delta_{c, \max}$, is attained at the pole of the boundary. The estimate for this thickness, derived in [6], corresponds to the expression

$$\frac{\delta_{c, \max}}{R} \sim Ra^{-1/6}$$

This estimate is valid for the range of values of modified Rayleigh number $Ra_I \leq 10^{16}$, which is of interest from the standpoint of nuclear reactor safety.

The thickness of shear layer on heat-insulated vertical walls in the $V_+$ region may be derived on the basis of the general theory of shear layer [5],

$$\frac{\delta_s}{R} \sim \sqrt{\frac{vR}{u_+}}$$

Here, $u_+$ is the characteristic magnitude of the large-scale flow velocity in the bulk of the $V_+$ region. The quantity $u_+$ is related to the characteristic value of large-scale nonuniformities of temperature in the $V_+$ region, $\delta T_+$, by the relation

$$u_+^2 \sim g\alpha \delta T_+ R$$
following from the condition of balance of momentum, where \( g \) is the acceleration of gravity, and \( \alpha \) is the coefficient of volume expansion. One more relation is derived from the condition of energy balance in the \( V' \) region,

\[
e_{p} u_{s} \delta T_{s} \sim Q R
\]  

(6)

where \( Q \) is the volume density of energy release by the fluid, \( e \) the specific heat, and \( p \) is the density.

We combine equations (4)-(6) to find the sought estimate for the thickness of boundary layer on heat-insulated vertical portions of the boundary in the \( V' \) region,

\[
\frac{\delta_{s}}{R} \sim Ra_{s}^{-1/6}
\]  

(7)

The slowest flow in the entire slice volume occurs in the stably stratified \( V' \) region. At first glance, in this case one must expect to find on the vertical heat-insulated walls the thickest boundary layer that would lead to strong disturbance of temperature distribution. However, the mechanism of boundary layer formation that comes into play in this case is entirely different from the conventional one and calls for special treatment. The thing is that, because of deceleration of flow near the wall due to viscous friction, additional heating of the fluid occurs in this region. An increase in the buoyancy force due to heating leads to a considerable degree to compensation of viscous drag, which, as feedback, brings about a restriction on the temperature rise. As a result, a peculiar temperature boundary layer emerges, which is fundamentally different from the normal one. We will estimate its thickness, \( \delta_{t} \), and the disturbance of temperature in it, \( \delta T_{s} \).

From the condition of balance of the forces of viscosity and buoyancy on the outside of the given boundary layer follows the relation

\[
\frac{vu_{s}}{\delta_{s}^{2}} \sim g \alpha \delta T_{s}
\]  

(8)

where \( u_{s} \) is the vertical component of velocity of flow outside of the boundary layer. Further, the requirement of energy balance on the outside of the boundary layer yields

\[
\frac{\lambda \delta T_{s}}{\delta_{s}^{2}} \sim Q
\]  

(9)

where \( \lambda \) is the thermal conductivity of the fluid.

We eliminate the quantity \( \delta T_{s} \) from the latter two relations to find

\[
\delta_{s} \sim \left( \frac{\lambda vu_{s}}{g \alpha Q} \right)^{1/4}
\]  

(10)

The equation of energy balance in the bulk of the \( V' \) region gives the estimate

\[
\rho c u_{s} \frac{T}{z} \sim Q
\]  

(11)

We substitute this estimate into (10) to derive

\[
\delta_{s} \sim \left( \frac{v' k'}{g \alpha T} \right)^{1/4}
\]  

(12)

Finally, we use the relation for the dependence of the temperature of the stably stratified \( V' \) region on the coordinate \( z \), derived in [6],

\[
T \sim T_{\text{max}} \left( \frac{z}{R} \right)^{4/5}
\]  

(13)

and the relations for the boundary layer in the cooled region of the boundary, analogous to (5) and (6), to find the estimate for the thickness of the temperature boundary layer in the stably stratified region of the slice volume,

\[
\delta_{t} \sim \delta_{c} \left( \frac{z}{R} \right)^{1/20} < \delta_{c, \text{max}}
\]  

(14)

where \( \delta_{c} \) is the thickness of the boundary layer in the cooled region of the boundary at \( z \sim R \).

We act as we did in deriving the latter formula and derive, from the system of relations (9), (10), the estimate of the temperature
disturbance in the boundary layers on vertical heat-insulated walls in the $V_\ast$ region,

$$\delta T_\ast \sim T \frac{\delta_t}{R} \left( \frac{R}{z} \right)^{1/6}$$

(15)

indicative of the smallness of this disturbance at $z > \delta_t$.

On comparing relations (3), (7), and (14), we see that the maximum thickness for all boundary layers in the slice volume is reached in the cooled region of the boundary at the pole. Therefore, in accordance with requirement (2) and the estimate (3), we arrive at the final condition of maximum correspondence of heat transfer in the slice volume to the prototype 3D-volume,

$$\frac{L}{R} >> Ra_i^{1/6}$$

(16)

In the range of values of the modified Rayleigh number of most interest from the standpoint of nuclear reactor safety, $Ra_i \sim 10^{12} - 10^{16}$, this condition corresponds to the inequality $L/R >> 10^{-2}$. Satisfying this condition in practice presents no difficulties.

4. Conclusion

The main results of this study are as follows.

The maximum possible correspondence of heat transfer in a heat-generating fluid of the simulating slice volume to that of the prototype 3D-volume consists in that the exponents in the power dependencies of the Nusselt number on the modified Rayleigh number coincide, while the coefficients of these dependencies, being functions of the angle $\theta$, differ from one another.

The condition of maximum correspondence consists in the requirement of smallness, as compared with the slice-volume thickness, of the thickness of boundary layers of three types. Of these, the first type is associated with the cooled region of the boundary, and the remaining two — with the vertical heat-insulated walls of the slice volume.

The condition of the maximum possible correspondence of heat transfer in a heat-generating fluid of the simulating slice volume to that of the prototype 3D-volume reduces to inequality (16) that represents the restriction from below on the slice-volume thickness depending on the modified Rayleigh number.

Nomenclature

$g$  
acceleration due to gravity (m s$^{-2}$)

$c$  
specific heat, J kg$^{-1}$

$Nu$  
Nusselt number, $Nu = \frac{qR}{\lambda T_{max}}$

$Nu_{up}$  
value of $Nu$ averaged over $S_{up}$

$Nu_{dn}$  
value of $Nu$ averaged over $S_{dn}$

$Q$  
volumetric heat generation (W m$^{-3}$)

$q$  
heat flux (W m$^{-2}$)

$R$  
curvature radius of pool boundary at the pole and characteristic space size of pool (m)

$Ra$  
Rayleigh number, $Ra = \frac{\alpha qTR^3}{\nu \lambda}$

$Ra_i$  
modified Rayleigh number, $Ra_i = \frac{\alpha qQR^3}{\nu \lambda \alpha}$

$S_{up}$  
upper flat part of the boundary faced down curved part of the boundary

$S_{dn}$  
fluid temperature (K)

$T$  
temperature maximum value in the fluid pool counted from the temperature of cooled boundary (K)

$T_{max}$  
characteristic value of large-scale nonuniformities of temperature in the $V_\ast$ region, (K)

$\delta T_\ast$  
disturbance of temperature in the boundary layer on a heat-insulated vertical wall in the $V_\ast$ region (K)
characteristic magnitude of the large-scale flow velocity in the bulk of the $V_+$ region (m s$^{-1}$)

vertical component of the velocity of flow outside of the boundary layer on a heat-insulated vertical wall in the $V_-$ (m s$^{-1}$)

upper and bottom parts of the fluid pool

vertical coordinate, counted from the pole level (m)

Greek symbols

$\alpha$ thermal expansion coefficient (K$^{-1}$)

$\delta_c$ the thickness of the boundary layer in the cooled region of the boundary at $z=3R$ (m)

$\delta_{c, \text{max}}$ the maximum thickness of the boundary layer in the cooled region of the boundary (m)

$\delta_+$ the thickness of shear layer on a heat-insulated vertical wall in the $V_+$ region (m)

$\delta_-$ the thickness of the boundary layer on a heat-insulated vertical wall in the $V_-$ region (m)

$\lambda$ thermal conductivity (W m$^{-1}$ K$^{-1}$)

$\nu$ kinematic viscosity (m$^2$ s$^{-1}$)

$\chi$ thermal diffusivity (m$^2$ s$^{-1}$)

$\rho$ density, kg m$^{-3}$

References


QUENCH OF MOLTEN ALUMINUM OXIDE ASSOCIATED WITH IN-VESSEL DEBRIS RETENTION BY RPV INTERNAL WATER

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ABSTRACT

In-vessel debris coolability experiments were performed in ALPHA program at JAERI. Molten aluminum oxide (Al₂O₃) was poured into a pool of water in a lower head experimental vessel. Post-test observation and measurement using an ultrasonic technique indicated the formation of the interfacial gap between the solidified Al₂O₃ and the vessel wall. Thermal responses of the vessel wall implied that the interfacial gap acted initially as a thermal resistance and water subsequently penetrated into the interfacial gap. The maximum heat flux at the inner surface of the vessel facing to the solidified Al₂O₃ was roughly evaluated to be ranged from 320 kW/m² to 600 kW/m². A post-test analysis was conducted with CAMP code. The influence of the interfacial gap on thermal behavior of Al₂O₃ and the vessel wall was examined.

1. INTRODUCTION

Severe damages of the reactor core followed by the molten debris relocation into the lower plenum were realized in the Three Mile Island Unit 2 (TMI-2) accident[1]. It was identified through the activities in the OECD TMI-2 Vessel Investigation Project (TMI-VIP) that approximately 19 tons of the debris was accumulated on the reactor pressure vessel lower head[2,3]. The results obtained from metallurgical examinations[4] showed that temperature of the lower head was locally elevated up to approximately 1100 °C and this temperature was sustained for approximately 30 minutes. It was also evaluated that the locally heated area was cooled down with a temperature reduction rate of 10 K/s to 100 K/s, resulting in the retention of the debris in the reactor pressure vessel.

The evaluation of the possibility of the in-vessel debris retention by the internal water of a reactor pressure vessel is crucial for the further clarification of a severe accident progression and the establishment of accident management measures to terminate the severe accident within the reactor pressure vessel. Related research is quite limited although this mode of the in-vessel debris retention was realized in the TMI-2 accident.

A possible inherent debris cooling mechanism within the lower plenum was proposed by Henry and Dube[5] in case that the sufficient water is available in the reactor pressure vessel. The creep deformation of a lower head at an elevated temperature and the penetration of water into a gap formed between the solidified debris and the lower head plays an important role in the hypothesized mechanism. The Korean program, SONATA-IV, was recently initiated[6], and several experiments have been performed in the international program organized by Fauske and Associates, Inc.[7] in order to clearly identify debris cooling mechanisms within the water filled lower plenum.

An experimental investigation on the in-vessel debris coolability by the reactor pressure vessel internal water was initiated in 1995 in ALPHA (Assessment of Loads and Performance of Containment in Hypothetical Accident) program conducted at JAERI (Japan Atomic Energy Research Institute)[8,9]. The phenomenological understanding on the in-vessel debris coolability and the identification of debris cooling mechanisms in a water-filled lower plenum are the primary objectives of the in-vessel debris coolability experiments. In parallel with the experiments, the development of CAMP (Coolability Assessment for Melt Pool) code is in progress for the detailed analysis on thermo-fluidodynamics of the molten debris associated with the in-vessel debris retention.

2. EXPERIMENTS

2.1 Experimental Apparatus and Procedures

A conceptual diagram of the in-vessel debris coolability experiments is shown in Fig. 1. The
experiments were performed in a model containment vessel of the ALPHA facility, which had an inner diameter of 4 m, a height of 5 m and an inner volume of approximately 50 m³. The experimental apparatus was mainly composed of a thermite melt generator, a lower head experimental vessel and water and nitrogen supply systems. The model containment vessel was pressurized during the experiments and nearly saturated water was used to suppress the occurrence of steam explosions.

Fig. 1 Conceptual Diagram of In-Vessel Debris Coolability Experiments in ALPHA Program

Only Al₂O₃ part produced by a thermite reaction of aluminum with iron oxides was used as a molten debris simulant. The thermite melt was produced in the thermite melt generator. The entrance of a melt delivery nozzle of the thermite melt generator, whose diameter was 0.11 m, was plugged with a layer of thick papers to assure the desired period of time (approximately 45 seconds) for separation between Al₂O₃ at the top and iron at the bottom of a thermite melt layer due to the density difference. After ablation or burning out of the paper layer, the molten Al₂O₃ was gravitationally introduced into the lower head experimental vessel. The vertical distance between the exit of the melt delivery nozzle and the bottom of the vessel was 0.65 m.

The lower head experimental vessel was composed of hemispherical and cylindrical parts with an inner radius of 0.25 m mainly fabricated by carbon steel. The inner surface of the hemispherical part was covered with a 2 mm thick stainless steel liner. Structure and the major dimensions of the vessel are shown in Fig. 2. A layer of thermal insulator was provided on the outer surface of the vessel for the minimization of heat loss to the surrounding atmosphere. The temperatures of Al₂O₃, a water layer and the vessel wall were measured by tungsten/rhenium (WRe) or chromel/alumel (CA) thermocouples. The locations of CA thermocouples on the vessel outer surface are also shown in Fig. 2. Additional two CA thermocouples were embedded in the vessel wall on the same radial axes with TV2 and TV3. Both thermocouples were located at 10 mm from the outer surface.

Fig. 2 Structure of Lower Head Experimental Vessel and Outer Surface Location of Thermocouples

2.2 Experimental Conditions

The major conditions of two experiments designated by IDC001 and IDC002 are listed in Table 1. The initial temperature and depth of a water layer in the lower head experimental vessel, and the ambient pressure in the model containment vessel at Al₂O₃ entry were similar in both experiments. These were approximately 450 K, 0.3 m and 1.3 MPa, respectively. Approximately 30 kg of Al₂O₃ in IDC001 and 50 kg in IDC002 were poured into the vessel. The temperature of the thermite melt was measured with a pyrometer in a separate test, resulting in approximately 2700 K at the Al₂O₃ surface immediately after the completion of the thermite reaction[10].
Table 1  Major Conditions of In-Vessel Debris Coolability Experiments

<table>
<thead>
<tr>
<th>Al₂O₃ Mass (kg)</th>
<th>Initial Water Depth (m)</th>
<th>Initial Water Temperature (K)</th>
<th>Ambient Pressure (MPa)</th>
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</thead>
<tbody>
<tr>
<td>IDC001 30</td>
<td>0.3</td>
<td>445</td>
<td>1.3</td>
</tr>
<tr>
<td>IDC002 50</td>
<td>0.3</td>
<td>450</td>
<td>1.3</td>
</tr>
</tbody>
</table>

2.3 Experimental Results and Discussions

2.3.1 Post-Test Observations

Post-test visual observations indicated that Al₂O₃ particles with smooth surfaces rested on the top of a continuous layer of the solidified Al₂O₃. The size of the Al₂O₃ particles was ranged from several millimeters to several centimeters. The mass of the particles was approximately 2.0 kg for IDC001 and 2.6 kg for IDC002, corresponding to approximately 7% and 5% to the total amount of Al₂O₃ poured into the lower head experimental vessel, respectively. It was not possible to judge whether those were formed by the thermal interaction between the molten Al₂O₃ and the water, or by quick solidification of molten Al₂O₃ droplets following the main stream of the molten Al₂O₃.

It was observed that the rough upper surfaces of the continuous layer of the solidified Al₂O₃ were formed in both experiments. This surface roughness resulted in the augmentation of surface area, which could have promoted heat transfer from the solidified Al₂O₃ to the overlying water layer. The formation of several cracks was also found over the upper surface of the solidified Al₂O₃ layer. The number of the cracks was fewer than expected, and each crack had a long horizontal length over the surface. These characteristics on the crack formation may suggest that once several incipient cracks were formed at weaker locations, the growth of those cracks was promoted due to the stress concentration.

As described earlier, the water penetration into a gap between a solidified debris and a lower head wall was proposed as a candidate of debris cooling mechanisms in a lower plenum region. The post-test visual observations indicated that a gap seemed to exist along the inner surface of the lower head experimental vessel near the upper surface of the solidified Al₂O₃. Attempts using an ultrasonic device were made to identify the existence and the spatial distribution of the gap between the solidified Al₂O₃ and the vessel wall for IDC002. Prior to the post-test measurement for the interfacial gaps, the applicability of the ultrasonic technique was confirmed with water-filled narrow gaps ranging from 0.3 mm to 1.0 mm formed by two parallel steel plates. Using ultrasonic waves with a frequency of 10 MHz or 15 MHz, the gap width was measured within an accuracy of ±15%.

In the post-test gap measurement with the ultrasonic technique, the signals to show the reflection of the ultrasonic wave at the solidified Al₂O₃ surface were detected at several spots on the lower head experimental vessel wall. The width of the gap in these spots was ranged from 1.0 mm to 2.0 mm using the detected time interval of reflected waves and a sonic velocity in water of 1500 m/s. However, no signal was detected at some spots on the vessel wall, implying that surface of the solidified Al₂O₃ was not preferable to the ultrasonic wave reflection, the width of the interfacial gap was narrowed, or the solidified Al₂O₃ adhered to the vessel wall at the corresponding spots.

The continuous layer of the solidified Al₂O₃ was removed from the lower head experimental vessel for IDC001. The bottom surface was generally smooth, being accompanied with several hollows, channels and crevices. The solidified Al₂O₃ layer was very brittle and easily broken. Through the visual observation of the broken solidified Al₂O₃ layer, it was found that a thin porous layer was formed between the core region and the outer shell layer. The outer shell layer with a thickness of approximately 5 mm through 10 mm was easily removed. Possible mechanisms to form the porous layer could be the quick generation of noncondensable gases dissolved in an effectively quenched part of the molten Al₂O₃ and the ingestion of water or steam through defects of the solidified Al₂O₃ layer. It is considered that this porous layer might have acted as a thermal resistance during the experiment. No remarkable thermal damage was found on the inner surface of the vessel used in IDC001.

2.3.2 Thermal Transient during Experiments

The temperature histories of Al₂O₃ measured by WRe thermocouple are plotted in Fig. 3. The location of thermocouple was at the vicinity of the lower head experimental vessel center axis at approximately 50 mm and 100 mm from the vessel bottom in IDC001 and IDC002, respectively. During the initial period of the experiments, the measured temperature was heavily oscillated. It was found that the temperature in IDC001 was slightly higher than that of IDC002 and a temperature decrease rate was quite similar in both experiments after the stable measurement was established.

The temperature decrease rate between 500 seconds and 1000 seconds was approximately 1.3 K/s. Assuming that this temperature decrease rate represented the whole amount of the solidified Al₂O₃ layer, the corresponding energy release rate
from Al₂O₃ was roughly evaluated to be 150 kW/m² for IDC001 and 190 kW/m² for IDC002 using surface areas available for heat transfer (approximately 0.32 m² for IDC001 and 0.42 m² for IDC002 neglecting surface roughness of the solidified Al₂O₃) and 1250 J/kg·K as a specific heat of solidified Al₂O₃. Because of insufficient locations for the temperature measurement it could not be concluded from the evaluated energy release rate that more effective cooling was established in IDC002.

![Graph showing temperature history of aluminum oxide](image1)

**Fig. 3 Temperature History of Aluminum Oxide Observed in Experiments**

The temperature histories on the outer surface of the lower head experimental vessel are shown in Fig. 4 for IDC001 and in Fig. 5 for IDC002. The calculated temperature on the vessel outer surface based on one-dimensional heat conduction through the vessel wall is also plotted in Fig. 5. Assumptions were made in the heat conduction calculation that the vessel outer surface was adiabatic and the inner surface was maintained at a contact temperature with molten Al₂O₃ at its melting temperature (2320 K). The contact temperature was theoretically evaluated, allowing the solidification of the molten Al₂O₃[11].

In both experiments, the highest temperature was indicated by the C/A thermocouple at the center axis of the lower head experimental vessel (TV3). Duration of a remarkable thermal transient depended on the location of the vessel and lasted for approximately 350 seconds in IDC001 and 550 seconds in IDC002 at vessel center axis. The final temperature of the vessel outer surface was almost identical with the saturation temperature of water at the ambient pressure (approximately 450 K).

The lower head experimental vessel temperature sharply increased due to interactions with the poured molten Al₂O₃ during the initial period. However, the observed temperature increase rate was much smaller than the theoretical calculation on heat conduction through the vessel wall. Small temperature increases were observed by TV1 and TV5 in IDC001 since the depth of Al₂O₃ was too shallow to affect these thermocouples. After the temperature at each measurement location rose up to the maximum, the vessel wall was cooled down with large temperature decrease rates. The temperature decrease was initiated earlier at the locations farther from the center axis of the vessel. The maximum temperature decrease rates during the thermal transient phase ranged roughly from 5 K/s to 6 K/s for IDC001 (except TV1, TV2 and TV5) and from 3 K/s to 4.5 K/s for IDC002 (except TV1 and TV5).

![Graph showing temperature history on outer surface](image2)

**Fig. 4 Temperature History on Outer Surface of Lower Head Experimental Vessel Observed in IDC001**

![Graph showing temperature history on outer surface with comparison](image3)

**Fig. 5 Temperature History on Outer Surface of Lower Head Experimental Vessel Observed in IDC002 and Comparison with Heat Conduction Calculation**

The formation of the interfacial gap and the thin porous layer at the vicinity of the solidified Al₂O₃ surface found in the post-test observation and gap measurement with the ultrasonic technique were supposed to be consistent with the observed thermal responses of the lower head experimental vessel wall. It was possible that the gap and the porous layer acted as a thermal resistance during the initial phase while the temperature increase was
shown over the vessel wall, and water penetrated into the gap in the later phase resulting in the effective heat removal from the vessel wall. Once a gap was initially developed at the interface upon the contact of the molten Al₂O₃ with the vessel wall, the gap width could be enlarged due to the thermal expansion of the vessel. As a result, the water penetration into the gap was considered to be enhanced (radial enlargement was approximately 1 mm assuming uniform temperature of the vessel wall at 800 K and no restriction for deformation). The difference in timing for the temperature decrease initiation on the vessel outer surface suggested the occurrence of the downward quenching of the vessel wall.

In spite of the same distance from the center axis of the lower head experimental vessel, three thermocouples, TV2, TV4 and TV6, indicated different timing for the initiation of the temperature decrease. In addition, the temperature decrease rate measured by TV2 in IDC001 was much slower than the other two locations. An unaxial symmetric configuration of the solidified Al₂O₃ layer, a spatial variation of the width of the interfacial gap, and/or a multi-dimensional flow pattern of water and steam in the gap might have influenced these thermal characteristics.

Several findings on the timing of temperature decrease initiation and the maximum temperature on the outer surface of the lower head experimental vessel are obtained from Figs. 4 and 5. Averaging arithmetically for the locations at 30 degrees from the vessel center axis (TV2, TV4 and TV6), both the initiation of temperature decrease and the maximum temperature were slightly larger in IDC002 than IDC001. At the vessel center axis, much higher temperature was observed in IDC002 at delayed timing. Another interesting finding is concerning the difference of the timing of temperature decrease initiation between locations at 30 degrees and the vessel center axis. A longer time was required in IDC002 until the cooling front arrived at the location of the center axis. It was supposed that the findings described above were influenced by the difference of Al₂O₃ mass between both experiments. The amount of 30 kg of Al₂O₃ in IDC001 and 50 kg in IDC002 formed an Al₂O₃ pool depth of approximately 0.11 m and 0.16 m at the center axis, respectively. Natural convective heat transfer coefficient decreases as a pool depth becomes shallow. Therefore, heat flux from the Al₂O₃ to the vessel through the solidified Al₂O₃ layer must have been lower in IDC001 than IDC002, resulting in a smaller steam generation when water penetrated into the interfacial gap. Based on a flooding theory for counter-current gas-liquid two-phase flow, a downward penetration of water could be easily established when an upward gas flow rate is small. In addition, it was possible that surface area of the solidified Al₂O₃ facing to the vessel wall (approximately 0.18 m² in IDC001 and 0.25 m² in IDC002) affected the water penetration into the interfacial gap. This is because the heat transfer area characterizes the steam velocity at the entrance of the interfacial gap.

The maximum heat fluxes at the inner surface of the lower head experimental vessel to reproduce the observed maximum temperature decrease rates on the vessel outer surface were approximated. The approximation was based on the radial heat conduction through the vessel wall by assuming a quadratic temperature profile. Approximately 550 kW/m² through 600 kW/m² for IDC001 and 320 kW/m² through 450 kW/m² for IDC002 were obtained.

The thermal transient behavior during the present experiments without an internal pressure load onto the lower head experimental vessel and a volumetric heat generation of the debris simulant in addition to the findings from the post-test observation and measurement suggested that incipient interfacial gaps were formed between the solidified debris simulant and the vessel wall upon a contact of both materials. The simulation of the decay heat generation in debris and the vessel deformation at an elevated temperature could be crucially important in order to obtain insights for detailed discussions on the possibility and conditions of the realization of the in-vessel debris retention by the internal water. The influences of these parameters are planned to be investigated in future in-vessel debris coolability experiments in ALPHA program.

3. POST-TEST ANALYSIS

A post-test analysis for IDC002 was performed with CAMP code for an analysis of thermo-fluidodynamics of a molten debris associated with the in-vessel debris retention. The development of CAMP are in progress at JAERI through modifying WINFLOW code designed for a thermo-fluidodynamic analysis of a gaseous flow in a reactor coolant piping[12]. A finite volume scheme for the spatial discretization, a semi-implicit time integration and hybrid analytical grids composed of triangle and rectangle cross-sectional cells were applied in CAMP. The present version of CAMP has capability of analyzing laminar and turbulent natural convection of a molten debris with an internal heat generation, solid-liquid phase change of the debris, heat conduction through a lower head wall and a thermal resistance of the interfacial gap between the debris and the lower head wall.
Boundary conditions of the post-test analysis for IDC002 are illustrated in Fig. 6. The initial temperatures of the molten Al₂O₃, the lower head experimental vessel wall and the water pool were set at 2500 K, 440 K and 468 K, respectively, based on the observations in the experiments and the separate measurement of thermite melt temperature. A cross-sectional noding view in the analysis is shown in Fig. 7. The three-dimensional structure was produced by rotating the cross-sectional noding at the center axis.

Fig. 6 Boundary Conditions in Post-Test Analysis with CAMP Code for IDC002

The influence of the interfacial gap on thermal transient characteristics was mainly investigated in the analysis. Due to a lack of models for the water penetration into the interfacial gap in CAMP, the analytical results could be compared with the initial phase of the experiment while the temperature of the lower head experimental vessel wall increased. The analytical results on the temperature history of Al₂O₃ and the comparison with the experimental result are plotted in Fig. 8. It is noted that the locations of the comparison were different. It is supposed that the analytical results at 0.1 m from the vessel bottom on the center axis was between those at 0.08 m and at 0.12 m. A large discrepancy between analysis and the experiment was found in the later phase where the water penetration into the interfacial gap was suggested to occur in the experiment. This discrepancy was resulted from the capability of CAMP that, as mentioned above, models for the water penetration have not been incorporated yet.

The influence of the interfacial gap on the variation of solid fraction in Al₂O₃ is shown in Fig. 9. A remarkable difference was not observed among three cases since a large amount of heat was removed from Al₂O₃ by boiling at the top surface of the solidified Al₂O₃ layer. The complete solidification of Al₂O₃ was predicted at approximately 700 seconds in these analyses which ignored the water penetration into the interfacial gap. As easily expected, the Al₂O₃ solidification could be completed earlier when the interfacial cooling by the penetrated water is appropriately modeled.

Fig. 7 Cross-Sectional View of Noding in Post-Test Analysis for IDC002

Fig. 8 Comparison of CAMP Analysis with Experimental Results in IDC002 for Temperature History of Debris Simulant

The results obtained from the post-test analysis on the temperature history of the lower head
Experimental vessel wall and the comparison with the experimental results are shown in Figs. 10 and 11. It was assumed that the gap with a uniform width of 1.0 mm (Fig. 10) and that of 2.0 mm (Fig. 11) existed at the interface between Al₂O₃ and the vessel wall. The better agreement of the analysis with the experiment for the temperature increase of the vessel wall was found in case that the gap width of 1.0 mm was assumed. However, the analysis overestimated the experimental result at the center axis (TV3) and underestimated at 30 degrees from the axis (TV2). In addition, the analysis could not reproduce the gradual reduction of temperature increase rates observed in the experiment. It is considered that these differences between the analysis and the experiment were mainly caused by the assumption that the gap with the uniform width existed over the whole area of the interface, and again the lack of modeling on the water penetration into the interfacial gap.

![Fig. 11 Comparison of CAMP Analysis with Experimental Results in IDC002 for Vessel Wall Temperature History (Gap Width : 2.0 mm)](image)

4. CONCLUSIONS

In-vessel debris coolability experiments were performed in ALPHA program at JAERI. Aluminum oxide as a debris simulant produced by the thermite reaction between aluminum and iron oxides was poured into a pool of nearly saturated water in the lower head experimental vessel.

Through the post-test visual observation and measurement using an ultrasonic technique, it was found that a thin porous layer at the vicinity of the surface of the solidified Al₂O₃ and the interfacial gap, ranging from 1 mm to 2 mm, between the lower head experimental vessel wall and the solidified Al₂O₃ were formed. The observed temperature histories of the vessel wall were consistent with the post-test observation and measurement. It is supposed that the interfacial gap and the thin porous layer acted as a thermal resistance during the initial phase while the temperature of the vessel wall increased, and subsequently water penetrated into the interfacial gap. The maximum heat flux at the inner surface of the vessel facing to the solidified Al₂O₃ was roughly evaluated in the range from 320 kW/m² to 600 kW/m².

In parallel with the experiments, the development
of CAMP code is in progress for the analysis of thermo-fluidodynamics of the molten debris. A post-test analysis was conducted with CAMP. The influence of the interfacial gap on the thermal transient of Al₂O₃ and the lower head experimental vessel was examined. The qualitative agreement between the analysis and the experiment was obtained for the temperature increase of the vessel wall by assuming the existence of the interfacial gap.

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DEDICATION

One of authors of the present paper, Mr. Norihiro Yamano, met with an untimely death at the age of 42 on October 3, 1997. This paper is dedicated to the memory of Mr. Norihiro Yamano, because the research activities on the in-vessel debris coolability in ALPHA program were directed with his deep knowledge, broad experience and great leadership. May his soul be in peace.

REFERENCES

Experimental Investigations on In-vessel Debris Coolability through Inherent Cooling Mechanisms

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Abstract
In order to investigate the possibility of in-vessel debris cooling through a narrow gap, a series of experiments, called LAVA(Lower-plenum Arrested Vessel Attack), are in progress. Tests have been performed using high temperature molten material to be relocated into the scaled vessel of a reactor lower head filled with water. An Al₂O₃/Fe thermite melt was used as a corium simulant. In these tests, the influence of internal pressure load on the lower head vessel wall and the materials of the melt simulant on gap formation were studied. As well, the thermal behavior of the vessel was examined. No indication of vessel failure was observed in any of the tests. In case the internal pressure was imposed, the lower head vessel experienced deformation at elevated temperatures and a thin gap formed around the interface between the solidified debris and vessel. This gap affected the initial temperature rise in the vessel but had little effect on the vessel cool down in the Al₂O₃/Fe thermite melt test. Preliminary results of the Al₂O₃ thermite melt test, however, show a significantly rapid temperature reduction rate in the lower head vessel compared with the Al₂O₃/Fe thermite melt tests, which indicates a possible cooling mechanism of water ingestion through the gap.

1. Introduction
A cooling mechanism due to boiling in gaps located between the debris crust and RPV(Reactor Pressure Vessel) wall was proposed for the rapid cooling of the lower head vessel in the TMI-2 accident1,2. The kernel of this mechanism is the non-adherence of the debris and the RPV wall when water is available in the lower head vessel. In addition to this, the material creep of the RPV wall under thermal/mechanical loads is to be considered. Evidence that the gap was formed when the hot molten material was poured into the steel vessel filled with water has been found in some relevant researches such as the FAI experiments sponsored by EPR13, the FARO experiments4 and the ALPHA(JAERI) experiments5. However, the cause and nature of this rapid cooling haven't been fully understood due to the complexities of the mechanisms and the difficulty of the experiments. A research program, called SONATA-IV(Simulation of Naturally Arrested Thermal Attack In Vessel)6, has been developed to investigate this inherent nature of degraded core coolability inside the lower head. As the first phase of the SONATA-IV program, the LAVA(Lower-plenum Arrested Vessel Attack) experiments have been being performed to gather
proof of gap formation between the debris and lower head vessel wall and to evaluate the gap effect on in-vessel cooling, using an Al₂O₃/Fe (or Al₂O₃ only) thermite melt as a corium simulant. In this study, the influence of two principal factors, internal pressure load on the lower head vessel and the material composition of the corium simulant, on the thermal behavior and material creep of the vessel have been evaluated. Temperature histories of the debris and lower head vessel were measured to analyze the potential of the material creep of the vessel wall, as well as the nature of the cooling process. As well, microstructure analyses of the vessel specimen were performed for a precise examination of the interface configurations between the debris crust and lower head vessel.

2. Descriptions of Experiments

A schematic diagram of the LAVA experimental facility is shown in Figure 1. The experiments are performed inside the pressure vessel, which has an inner diameter of 2.4 m and a height of 4.8 m. The experimental facility is composed of a thermite melt generator, a melt holder, a test section of lower head vessel (LHV) and a gas supply system. The Al₂O₃/Fe thermite melt is used as a corium simulant. After the thermite melt is generated in the furnace, it is first delivered to the melt holder, and then delivered into the LHV through the melt delivery nozzle, whose inner diameter is 0.08 m. The vertical distance between the exit of the delivery nozzle and the bottom of the LHV is 0.64 m. The LHV is made of carbon steel (SA516-Gr.70) and is composed of hemispherical and cylindrical parts with inner diameter of 0.5 m and thickness of 0.025 m.

The experiments were performed under an elevated pressure of about 1.8 MPa and a low subcooled water condition (about 50 K subcooled water) to suppress the possibility of a steam explosion. Depending on the test conditions, to achieve the necessary internal stress in the RPV wall, the pressure of the lower chamber below the LHV is maintained at a 0.1 MPa by installation of a connection path to the atmosphere. The temperature histories of the debris and LHV were measured by W/Re and K-type thermocouples, respectively. The W/Re thermocouple is installed at the vicinity of the LHV center axis and the 5 cm upper position from the bottom. The K-type thermocouples are embedded in the 2mm depth of the LHV outer surface. Figure 2 shows the locations of the K-type thermocouples in the LHV outer surface. The downward deflection of the LHV is measured by a linear displacement measurement device, whose measuring error bound is ± 0.002 mm. After the quenching process has finished, the test section is supposed to be cut along a center line by a 1 mm thick band saw for a precise examination of the interface configurations between the debris crust and lower head vessel. Also, metallurgical inspections of the debris and lower head vessel samples are executed to observe the structural deformations.

3. Experimental Conditions

Four tests, LAVA-1, LAVA-2, LAVA-3 and LAVA-4, have been performed to date, as specified in Table 1. Melt simulant generated by Al₂O₃/Fe thermite reaction was poured into subcooled water in the LHV under high pressure. The thermite had composition of Fe₂O₃ 75 % and
Al 25 % by weight and its initial melt temperature is estimated to be 2700 K based on the stoichiometric chemical reaction. In the LAVA-1 test, 40 kg of Al₂O₃/Fe thermite melt was delivered into 55K subcooled water in the vessel without the pressure load onto the LHV wall. The experimental conditions of the LAVA-2 test were similar to those of the LAVA-1 test except for the existence of an internal stress on the LHV wall. In the LAVA-3 test and the LAVA-4 test, the effect of the melt composition was examined. Using a melt holder designed to separate iron and alumina, only an Al₂O₃ melt was used as a corium simulant. In the LAVA-3 test, however, the iron was not fully separated and a small amount of iron (about 3.5 kg) was introduced into the vessel. The LAVA-4 test was repeated again with an improvement of melt separator. An internal steel grid, fabricated as a 0.3 m diameter disk with 48 holes 0.02 m in diameter, was used only when using an Al₂O₃/Fe melt for disrupting the potential of the jet impingement effect of the iron melt which was highly superheated.

<table>
<thead>
<tr>
<th></th>
<th>LAVA-1</th>
<th>LAVA-2</th>
<th>LAVA-3 / LAVA-4</th>
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<tr>
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<td>Al₂O₃ / Fe, 40 kg</td>
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<tr>
<td>Internal Grid (Yes or No)</td>
<td>Yes</td>
<td>Yes</td>
<td>No</td>
</tr>
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</table>

4. Experimental Results

Gap Formation

High temperature molten material draining into the test section of LHV results in debris bed formation with a pressure increase due to the evaporation of water by the melt. In the Al₂O₃/Fe thermite experiments of LAVA 1 and LAVA 2, molten iron is the first material to drain out of the thermite melting furnace, which has the potential for substantial melt ablation of the LHV due to a high superheated temperature. In real situation of the severe accident, however, the initial core material that would drain into the lower plenum would be oxidic material which is not much superheated. Post-test observations of the test vessel showed that the iron layer was located up to around 30 degree position in the LAVA 1&2 tests and 15 degree in the LAVA-3 test. The aluminum oxide layer was accumulated above the iron layer with a clear separation and was leveled up to around 60 degree position in the LHV in all the test. On the top of a continuous layer of the solidified debris a small amount of the debris particles rested. The mass of the debris particles was approximately 0.8 kg for LAVA-1, 2.8 kg for LAVA-2, 4.2 kg for LAVA-3 and 3.3 kg for LAVA-4, whose diameter ranged from several millimeters to several centimeters. The debris particles were mainly composed of aluminum oxide, which indicates that the iron melt formed a stable molten layer due to unbroken melt jet and re-agglomeration. On the contrary, the alumina melt was partly fragmented by the break-up of the leading edge zone and erosion of the column.
According to the cross-sectional views of the LHV in Figure 3, even though the LHV did not fail, the iron welded to the inner surface of the LHV and the vessel experienced ablation to about 5mm in the LAVA-1 test. The metallurgical test of the microstructure and mechanical analysis for the LHV specimens from the LAVA-1 test show the structure change varying from a subcritically heat-treated structure to melted structure. Figure 4 shows the change of the mechanical property along the thickness of the LHV wall from the outer surface in the LAVA-1 test. The depth of the molten zones, represented by a much lower strength than the zones in which the peak temperature was below the melting point, is in general agreement with the ablated depth of the LHV observed by visual inspection of the cross-section of the vessel.

Contrary to the results of the LAVA-1 test, a post-test examination of the LHV in the LAVA-2 test shows a clear gap between the melt crust and vessel wall. The gap size ranged from 0.6 mm to 1.5 mm, depending on location, but at some points a small amount of RPV ablation was observed. Since the experimental conditions of the LAVA-2 test were similar to those of the LAVA-1 test except for the existence of an internal pressure load on the LHV wall, this difference of gap formation might result from this internal stress across the vessel. Both the internal pressure load on the vessel and elevated temperatures might lead to a deformation of the vessel, but the molten material crust is not subject to the same pressure stresses, which are supposed to enhance the gap formation between the debris and LHV. In some pieces of the test specimen of the LAVA-2 test, the vessel wall and iron layer were separated easily and small pores were found in the surface of the debris. Cross-sectional view of the Al$_2$O$_3$ layer shows highly porous uniform debris bed inside and a dense porous layer outside with a clear gap near the LHV wall. In LAVA-3 test, where about 3.5 kg iron melt drained first into the LHV due to early failure of melt plug in the melt separator, about 1.6 mm gap was formed between the solidified Fe melt and the LHV, as shown in Figure 3 (c). And, lots of porosity was detected inside the solidified Fe melt layer. The continuous Al$_2$O$_3$ layer in the LAVA-3 test was removed from the LHV and broken easily with a small impact. The contact surface of the Al$_2$O$_3$ layer with the LHV was generally smooth, with small pores and crevices. Preliminary observations on the solidified melt layer in the LAVA-4 test using the ultra-sonic technique shows that a gap seemed to exist along the inner surface of the LHV.

Figure 5 shows the measured deformation distance of the LHV bottom center point. In LAVA-2 test, the maximum deformation of 2.7 mm was detected. Since the displacement gauge was damaged due to the high temperature of the surroundings, displacement was measured up to 140 sec from the start of the data acquisition. In the following tests, the device was connected to the bottom of the LHV through a 3.2 mm diameter and 0.35 m length stainless steel bar to protect the device from the high heat of the surrounding. According to the measured values of the LHV deformation in Figure 4, the maximum displacement length measured for the LAVA-3 and LAVA-4 tests are about 5.4 mm and 3.1 mm, respectively. The shapes of the deformation curves are similar to those of temperature histories at the outer surface of the vessel, which implies that the vessel expanded due to the thermal load inside the vessel. After the LHV had expanded to the maximum point, the vessel slowly contracted and then the contraction stopped with some displacement from the initial point. It is not certain, however, that the measured values of the final displacement
represent the plastic deformation of the LHV at this point. It will be confirmed by the metallurgical test of the vessel wall specimen and the structural analysis using ABAQUS code, which are in progress.

**Cooling of debris and LHV**

Figure 6 shows the debris temperature histories of the LAVA-1, LAVA-3 and LAVA-4 tests measured by W/Re thermocouples. Measurement locations are initially 5cm upper position from the bottom surface along the LHV center axis. In the LAVA-4 test, another W/Re thermocouple was installed at the bottom of the LHV inner surface. In the experiments, however, the actual measuring point of the debris temperature is uncertain, due to the violent interaction during melt relocation into water. During the initial period of debris cooling, the temperature reduction rates of debris were 6 K/s for the LAVA-1 test, 1.4 K/s for the LAVA-3 test and 1.3 K/s for LAVA-4 test. Explanation of the large difference in the cooling rates is not possible without detailed analyses of the energy balance of the debris bed. Possibly, the reason is that the aluminum oxidic layer where the temperature was measured in the LAVA 3&4 tests has much lower thermal conductivity than the iron metal layer in the LAVA-1 test.

Figure 7 show a series of temperature histories measured at the outer surface of the LHV. Despite the same distance from the center axis, the thermocouples at a same latitude showed different temperature histories in each test. This can be explained by unaxisymmetric configurations of the solidified debris, which were observed in the post-test visual examination. Variations in temperature along the longitude would be quite dependent on the cooling process of the debris bed.

Comparing LAVA 1 and LAVA-2 tests, the highest temperature of the LAVA-2 test is lower than that of the LAVA-1 test by 300 K and also the heat-up rate is slower. The averaged temperature reduction rate of LHV, however, is similar to the rate in the LAVA-1 test by 0.25 K/s. This result implies that the gap formed at the LHV wall in the LAVA-2 test affected initial temperature rise of the vessel but had little effect on the temperature reduction rate of the LHV during the cool down period. In the LAVA-3 test, the highest temperature of the vessel at the center axis is 1223 K. Compared to the bottom point, the temperatures at the 15° upper positions were lowered by 50 - 80 K and those at the 30° upper positions by 320 - 570 K. Especially, at a certain azimuth angle of thermocouple location of L5 and L9, the temperature reduction rates are steep and the temperature differences along the angle are larger than those of the other positions. These differences seem to be due to the enhanced cooling of the aluminum oxidic layer which was formed unaxisymmetrically at those positions.

Compared with the Al2O3/Fe melt tests(including LAVA-3 test), the LAVA-4 test shows significantly different thermal behavior of the LHV. Figure 7 (d) shows that the LHV in the LAVA-4 test experienced rapid cool down. The temperature reduction rates of the vessel were from 1.54 K/s to 4.38 K/s, which were ten to twenty times larger than those of the other tests. It was inferred from this result that a gap was formed between the debris and the LHV and the water penetration into the gap enhanced the cooling of the LHV. The difference in the timing of the temperature decrease initiation indicates the occurrence of the downward quenching of the LHV wall.

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Syntheses of Test Results

The above experimental results are summarized in Table 2. The gap formation between the continuous solidified debris bed and the LHV wall was clearly shown with post-test examination. When the molten material is poured into the vessel filled with water, contact resistance with crust formation of molten material should be established at the interface and then the gap could be kept by the combined effect of the trapped water vaporization in the crevices of the LHV wall and the vessel expansion due to internal pressure and the heat up. Internal pressure was a key factor to generate a gap in molten iron metal layer which was highly superheated. On the other hand, the aluminum oxide layer easily formed the gap in all the cases, because of the differences in material property and the relatively low superheat of aluminum oxide melt. The vessel deformation was detected during the tests, but the relationship between the vessel deformation and the gap formation is not clear yet. Currently the metallurgical test for the reactor vessel wall and structural analysis is in progress to characterize the mechanism of the gap formation.

Table 2. Experimental Results

<table>
<thead>
<tr>
<th>Test</th>
<th>LAVA-1</th>
<th>LAVA-2</th>
<th>LAVA-3</th>
<th>LAVA-4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Melt Cooling Rate (K/s)</td>
<td>6.6</td>
<td>-</td>
<td>1.4</td>
<td>1.3</td>
</tr>
<tr>
<td>LHV Max. Temp.(K)</td>
<td>1517*</td>
<td>1195*</td>
<td>1224</td>
<td>1068</td>
</tr>
<tr>
<td>LHV Avg. Cooling Rate (K/s)</td>
<td>0.24</td>
<td>0.247</td>
<td>0.286</td>
<td>3.28</td>
</tr>
<tr>
<td></td>
<td>0.491(TC:L5)</td>
<td>0.523(TC:L9)</td>
<td>1.54(TC:L1)</td>
<td>4.38(TC:L9)</td>
</tr>
<tr>
<td>Gap Formation (Y/N)</td>
<td>N</td>
<td>Y(0.6-1.5mm)</td>
<td>Y(1-2mm)</td>
<td></td>
</tr>
<tr>
<td>LHV Deformation (mm)**</td>
<td>-</td>
<td>2.7***</td>
<td>5.4</td>
<td>3.1</td>
</tr>
</tbody>
</table>

*: Temperature at the 0° position could not be measured.
**: Values measured by the linear displacement measurement device.
***: The measuring device was destroyed during operation.

Temperature history of the molten pool debris bed and the LHV wall shows quite different thermal behavior depending on the experimental conditions. Figure 8 shows the comparison of the temperature histories of the LHV at the same latitude. In LAVA-2 and LAVA-3 tests, the differences of the temperature increase rate and the maximum temperatures indicate the influence of the gap formation, but the characteristics of the vessel cool down were not changed, compared to the LAVA-1 test where no gap was formed. This result implies that there was no water ingestion into the gap and couldn't cool down the RPV wall effectively even if a gap was formed. On the other hand, in the $Al_2O_3$ melt test (LAVA-4 test), a rapid cooling of the RPV wall was established and also showed a characteristic of quenching. The reason would be that the aluminum oxide layer is a high porous debris bed with a large gap at the interface with the RPV wall, which enables water to penetrate into the gap easily and cool the wall effectively. The possibility of the rapid cool down of the LHV in the test using $Al_2O_3$ melt was shown in the ALPHA program[6] performed by JAERI(Japan Atomic Energy Research Institute), also. For a precise analysis of the thermal behavior of the vessel, integral analyses of energy balance between molten pool and the vessel wall are in progress.
6. Conclusions

Experiments have been performed using Al$_2$O$_3$/Fe (or Al$_2$O$_3$ only) thermite melt to investigate the effects of the internal pressure load inside the vessel and of molten material on the gap formation and cooling of the RPV wall. From the results of temperature history measured in the LHV wall and visual examination of the structure inside the debris bed, it is inferred that a gap formed at the interface between the debris crust and LHV wall even in the iron layer when the internal pressure was imposed. This gap affected initial heat-up of the vessel but couldn't ensure the cooling of the LHV wall. A significantly rapid temperature reduction occurred only in the Al$_2$O$_3$ thermite melt test, which is supposed for water to penetrate into the gap and cool the vessel effectively. For clear confirmation of the inherent cooling mechanisms, the experiments will be performed with various initial conditions, especially the depth and the subcooling of water. Also, analytical investigations using ABAQUS code are in progress to examine the thermal behavior and possibility of creep deformation of the lower head vessel in addition to the metallurgical test of test specimen.

References


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Figure 1. Conceptual Diagram of LAVA Experimental Facility

Figure 2. Thermocouples Locations on the Lower Head Vessel Surface

(a) LAVA-1 Test  (b) LAVA-2 Test  (c) LAVA-3 Test

Figure 3. Cross-sectional View of the Lower Head Vessel
Figure 4. Mechanical Property Distribution in LAVA-1 Test

Figure 5. Measured Values of the Lower Head Vessel Deformation

Figure 6. Melt Temperature History in the Lower Head Vessel
Figure 7. Temperature History at the Surface of the Lower Head Vessel

Figure 8. Temperature Comparison at the same Locations of the Lower Head Vessel
FOREVER Experiments on Thermal and Mechanical Behavior of a Reactor Pressure Vessel during a Severe Accident


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Abstract. This paper describes the FOREVER (Failure Of REactor VEssel Retention) experimental program, which is currently underway at the Division of Nuclear Power Safety, Royal Institute of Technology (RIT/NPS). The objectives of the FOREVER experiments are to obtain data and develop validated models (i) on the melt coolability process inside the vessel, in the presence of water (in particular, on the efficacy of the postulated gap cooling to preclude vessel failure); and (ii) on the lower head failure due to the creep process in the absence of water inside and/or outside the lower head.

The facility employs 1/10th-scale carbon steel vessels of 0.4m diameter, 15mm thickness and 600mm height. Up to 20 liters of binary-oxide melts with 100-300 K superheat are employed, as a simulant for the prototypic corium melt, and internal heating is provided by electrical heaters of up to 20 kW power in order to maintain the vessel wall temperatures at 1100-1200K. Auxiliary systems are designed to provide an overpressure up to 4 MPa in the test vessel. Thus, severe accident scenarios with RCS depressurization are modeled.

Creep behavior of the three-dimensional vessel, formation of the gap between the melt pool crust and the creeping vessel, and mechanisms of the gap cooling by water ingestion will be the subjects of study and measurements in the FOREVER experimental program. Scaling rationale as well as pre-test analyses of the thermal and mechanical behavior of the FOREVER test vessels are presented.

1 Introduction and background

The present study is concerned with the phenomena of melt-vessel interactions during a postulated severe accident in a light water reactor. The study is focused, in particular, on the thermal and mechanical response of the reactor pressure vessel (RPV) during the late phase of in-vessel core melt progression when coolability and in-vessel molten core retention issues become important.

Previously, analytical research on RPV creep deformation and rupture has been performed at EPRI (Anderson et al.), SNL (Chambers, 1987; Dosanjh and Pilch, 1991), INEL (Shah, 1986, Thennes et al., 1988-1989, Rempe et al., 1993-1994, Chavez and Rempe, 1994), GRS (Gruner and Schulz, 1989), ORNL (Hodge and Ott, 1989, Hodge et al., 1991), UWM (Witt, 1994), UCSB (Theofanous et al., 1996), PSI (Duijvestijn et al., 1997), SNUK (Kwang et al., 1997) and, in the EU-funded REVISA and RPVSA projects.

On the experimental side, uniaxial tensile tests were conducted at KfK (Müller and Kuhn et al., 1991), INEL (Thennes et al., 1994), CEA (Sainte, 1995) and RRC-KI (Degaltsev et al., 1997).
1997) to obtain data on the creep properties and rupture of the vessel steel. In RUPThER experiments (France), multiaxial creep rupture tests are performed using pressurized thin shell carbon steel cylinders in the temperature range up to 1000°C. Recently, experiments have been performed at SNL on creep failure of relatively large vessels, held at a pressure of 100 bars, while the vessel bottom head is heated to temperatures of 1000K (Chu et al., 1997). Experiments have also been conducted at JAERI (Maruyama et al., 1996), KAERI (Kim et al., 1997), RRC-KI (Asmolov et al., 1997), FAI (Henry and Hammersley, 1996) and TUM/LAT (Zeisberger et al., 1997) on the gap-cooling phenomenon. No conclusions about the feasibility of the gap-cooling mechanism have been reached, so far.

The present study aims to enlarge the current data base and to improve the knowledge base in order to address the following questions:

- Can the creep-induced gap opening between the core debris and the vessel wall, and the water ingestion into the gap, together, serve as an inherent cooling mechanism to prevent vessel failure?
- If no cooling occurs, what is the mode of failure and how long will it take to fail the RPV lower head?

2 Experimental program and test facility

2.1 FOREVER experimental program

The current FOREVER program includes three major test series. In the first series FOREVER/C, we will investigate the vessel deformation and creep behavior under thermal attack by an oxidic-melt pool, Fig.1a. The focus is placed on physical mechanisms which govern the debris-vessel gap formation. In addition, data will be obtained on the creep rate at several locations on the lower head, which could be employed for validation of creep models and codes. In the contrast to SNL LHF experiments (which simulate the TMI-2 scenario with 10 MPa pressure loading), depressurized scenarios are experimentally simulated in the FOREVER tests.

The second series FOREVER/G is devoted to the gap cooling phenomenology. Water will be supplied to the top of the melt pool after the vessel creep has occurred to a certain extent. Water ingestion into the gap between the melt pool crust and the creeping vessel is detected by thermocouples, mounted on the inner surface of the vessel wall, Fig.1b.

In the third series FOREVER/P, effects of penetrations on the vessel deformation and creep will be investigated, Fig.1c. In particular, data will be obtained on two possible vessel failure modes related to penetrations: (i) weld failure and drop-off of a penetration tube in a Swedish BWR, (ii) weld failure and opening of a hole around a penetration during the vessel creep process. Vessels with typical LWR penetrations will be manufactured and subjected to thermo-mechanical loadings.

The present paper is devoted to the design, pre-test analysis, and scaling rationale of the FOREVER/C tests, scheduled to be performed in the first half of 1998.
Figure 1: Schematic of the FOREVER/C (a), FOREVER/G (b) and FOREVER/P (c) tests. Design of the pressure vessel (d).

2.2 FOREVER test facility

The facility employs 1/10th-scale carbon-steel vessels 400mm diameter, 15mm thick and 600mm high, Fig.1d. The auxiliary systems are designed to provide an overpressurization up to 4 MPa in the test vessel. Thus, severe accident scenarios with RCS depressurization are modeled. Up to 20 liters of binary-oxidic melts with 100-300 K superheat are employed, as simulant for the prototypic corium melt. The temperature difference between the melt liquidus and solidus is about 50K and the liquidus point ranges from 1300K to 1400K.

The high-temperature (up to 1700K) oxide melt is prepared in the Si-C-crucible of a 50kW induction furnace and is, then, poured into the test section. The pressure vessel may be heated to a designated temperature, prior to the melt delivery. Melt injection equipment is then removed by a remotely-controlled arm and the test section is closed by a motor-driven pneumatic ball valve. Specified overpressurization is then supplied with an inert gas supply. In order to assure
safe test performance all the test equipment are installed inside a concrete containment with 40 cm thick walls; Fig. 2.

Figure 2: Schematic of the FOREVER/C experimental set-up.

Table 1: Parameters chosen for the FOREVER/C-1 test

<table>
<thead>
<tr>
<th>%</th>
<th>Parameter</th>
<th>Unit</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Initial temperature of the melt</td>
<td>°C</td>
<td>1250</td>
</tr>
<tr>
<td></td>
<td>Initial temperature of the vessel</td>
<td>°C</td>
<td>400</td>
</tr>
<tr>
<td></td>
<td>Power input</td>
<td>kW</td>
<td>20</td>
</tr>
<tr>
<td></td>
<td>Volumetric power input</td>
<td>MW/m³</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>Heat flux on the wall (MAX/MIN), (calc., 5 h)</td>
<td>MW/m²</td>
<td>~ 0.1/0.03</td>
</tr>
<tr>
<td></td>
<td>Max. vessel temperature (INNER/OUTER) (calc., after 5 h)</td>
<td>K</td>
<td>~ 1222/1193</td>
</tr>
<tr>
<td></td>
<td>Max. melt pool temperature (calc., after 5 h)</td>
<td>K</td>
<td>1487</td>
</tr>
</tbody>
</table>

A number of B-type and K-type thermocouples are used to measure the temperature of the melt (debris) at different locations in the hemispherical pool and to determine the thermal response of the pressure vessel. The vessel deformation and creep are measured by position transducers. Up to 20 linear displacement transducers (LDT) are mounted at five latitude locations of the hemispherical lower head and used to measure the creep behavior of the three-dimensional vessel. Typically, at each level, three LDTs are deployed. Two of these measure local (vertical/horizontal) displacements, and another monitors an integral (longitude-around) displacement.
3 Design calculations and scaling rationale

3.1 Thermal loadings prediction by the MVITA code

Figure 3: Evolution of crust formation and melt pool temperature, calculated by the MVITA code.

The MVITA model developed at RIT/NPS is employed to calculate the thermal processes in the FOREVER test vessel; [1] [2] [3]. Initial melt and vessel temperatures, vessel cooling boundary conditions and internal heating rate were varied in a parametric study, while taking into account capabilities of of the facilities at the RIT/NPS laboratory. As a result, test conditions were optimized and chosen for the FOREVER/C-1 test. These are shown in Table 1.

Typical calculated temperature fields are depicted in Fig.3. The heat flux distributions along the inner surface of the pressure vessel (PV) are shown in Fig.4. Fig.5 presents the development of temperature profiles along the inner and outer surface of the vessel wall. As can be seen, very soon after the melt-vessel contact both the inner and outer surfaces of the vessel wall are in the temperature range where a drastic reduction of the mechanical strength of the vessel carbon steel occurs.
3.2 Mechanical loadings prediction by the ANSYS code

For the FOREVER vessel structural analysis, the ANSYS code [4] is employed. ANSYS is a commercial finite-element (FEM)-structural mechanics code. The code is able to perform linear and non-linear static or transient structural analyses, coupled with thermal analyses, in a two- or three-dimensional formulation. Both small- and large-strain evaluations are allowed.

The method of initial strains, modified for strain hardening creep (Kraus, 1980 [5]), is employed for creep calculations. Although several creep laws and creep-data-fitting-correlations have been reported in the literature, the following Bailey-Norton creep law (with exponential multiplier to represent temperature dependence) is used here:

$$\varepsilon_{\text{creep}} = 6.055 \cdot 10^{-26} \cdot \sigma^{4.72} \cdot t^{0.85} \cdot e^{-\frac{87200}{T}}$$  \hspace{1cm} (1)$$

where $T$ is given in K, $\sigma$ in Pa and $t$ in sec. The coefficients in eq.(1) are adopted from Mielela et al., (1995) [6]. This creep fitting was obtained for reactor vessel steel (20MnMoNi55), in the temperature range from 600°C to 1000°C.

In this work, temperature distributions, obtained from the MVITA calculations, for different time instants, are interpolated to the ANSYS computational nodes. A number of calculations were performed to assess the vessel thermal strain, the vessel elastic, plastic deformations and the vessel creep under different pressure and thermal loading conditions. It was found that under the chosen conditions (Table 1) the FOREVER vessel may experience a displacement of 3-6mm at 2 MPa internal pressure after 4-5 hours. A similar displacement may be obtained at $\simeq 0.75$ hours if the vessel internal pressure is 4 MPa (Fig. 6). It can be seen that the vessel expands both sidewards and downwards. The maximum strains in the lower head are observed in the angle 45-60° of the hemisphere. Mainly, this is caused by the highest heat fluxes in the melt pool corner, and, therefore, the high vessel temperatures and the largest reduction in vessel strength in this region. The calculated results help to determine parameters for displacement-measuring devices (LDTs) in the FOREVER/C-1 test.
Figure 5: History of the temperature distribution along the inner and outer surfaces of the FOREVER pressure vessel

3.3 Scaling rationale

Table 2 summarizes scaling ratios of the values of the most important geometrical, thermal and mechanical loading parameters for a prototypic reactor case and for the FOREVER/C test. Since the vessel creep deformation and gap opening are the focus in the FOREVER/C test series, the current scaling considerations are limited to the effective stresses, their components and distributions, as well as the vessel strain and gap formation. The scaling methodology of the gap cooling and the penetration failure have yet to be developed for the future test series, FOREVER/G and FOREVER/P.

From Table 2 it can be seen that, with the vessel geometry and test conditions chosen, membrane stresses are modeled exactly, while the thermal stresses are not. More importantly, however, in the FOREVER/C test the stress distribution is dominated by the thermal stresses, having a maximum value in the region of 45-60°, as in the prototypic reactor accident. This is the major difference between the FOREVER and Sandia LHF experiments (Chu et al., 1997). The internal pressure-induced membrane stresses dominate the creep and rupture processes in the LHF experiments, while thermal stresses dominate these processes in the FOREVER/C experiments.
Table 2: Scaling consideration of the FOREVER/C tests

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Scaling Parameter</th>
<th>FOREVER/C Exp.</th>
<th>Reactor case</th>
<th>Ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>Geometry</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Shape</td>
<td></td>
<td>Hemispherical lower head + Cylindrical part</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Inner diameter, m</td>
<td>≈ 0.4</td>
<td>≈ 4</td>
<td>1:10</td>
<td></td>
</tr>
<tr>
<td>Wall thickness, m</td>
<td>0.015</td>
<td>≈ 0.15</td>
<td>1:10</td>
<td></td>
</tr>
<tr>
<td>Volume of the lower head, l</td>
<td>≈ 17</td>
<td>≈ 1.7·10⁶</td>
<td>1:1000</td>
<td></td>
</tr>
<tr>
<td>Thermal loading</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Vessel wall temperature, K</td>
<td>1000 ... 1250</td>
<td>1000 ... 1700</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>Heat flux, MW/m²</td>
<td>0.03 ... 0.1</td>
<td>0.03 ... 0.1</td>
<td>1:1</td>
<td></td>
</tr>
<tr>
<td>Vessel wall temp. drop, K</td>
<td>15 ... 50</td>
<td>150 ... 500</td>
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<td></td>
</tr>
<tr>
<td>Mechanical loading</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Internal pressure, MPa</td>
<td>2</td>
<td>2</td>
<td>1:1</td>
<td></td>
</tr>
<tr>
<td>Deadweight pressure, MPa</td>
<td>negligible</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Membrane stresses σₚ, MPa</td>
<td>≈ 13</td>
<td>≈ 13</td>
<td>1:1</td>
<td></td>
</tr>
<tr>
<td>Effective thermal stresses (max) σₚmax, MPa</td>
<td>30 ... 100</td>
<td>300 ... 1000</td>
<td>1:10</td>
<td></td>
</tr>
<tr>
<td>Stress ratio: σₚ/σₚmax</td>
<td>1:3 ... 1:10</td>
<td>1:20 ... 1:100</td>
<td>10:1</td>
<td></td>
</tr>
<tr>
<td>Vessel deformation and creep</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Maximum strain (rupture), %</td>
<td>16 ... 20</td>
<td>16 ... 27</td>
<td>≈ 1:1</td>
<td></td>
</tr>
<tr>
<td>Maximum displacement, mm</td>
<td>0 ... 15(rupture)</td>
<td>0 ... 150(rupture)</td>
<td>1:10</td>
<td></td>
</tr>
<tr>
<td>Angle of maximum strain, °</td>
<td>45 ... 60</td>
<td>45 ... 60</td>
<td>1:1</td>
<td></td>
</tr>
</tbody>
</table>

4 Summary

An ambitious experimental program on the reactor pressure vessel creep and gap cooling has been launched at the Division of Nuclear Power Safety, Royal Institute of Technology (RIT/NPS). Interactions between a high-temperature oxidic melt and the hemispherical carbon steel vessel will be experimentally investigated in the 1:10 scale FOREVER facility. The vessel long-term creep, gap formation between the oxidic crust and the creeping vessel, and gap cooling are investigated in an integral fashion, in the FOREVER experiments.

Pre-test calculations are performed by using the MVITA and ANSYS codes to determine the thermal and mechanical loadings. Parametric investigations allow identification of the important factors to consider in the test procedure and test conditions. A scaling rationale is being developed for each test series to ensure the relevance of the data obtained to reactor prototypic accident conditions.

The design and construction of the FOREVER test vessels, of the auxiliary systems as well as the design and testing of instrumentation has been completed. The first test in the FOREVER/C series is scheduled for early 1998. We believe that the FOREVER tests will provide crucial data and knowledge base to advance the current understanding of (i) the in-vessel coolability phenomenology, and (ii) the vessel creep behavior.

References


Figure 6: The FOREVER vessel creep behavior at different times of transient, calculated by the ANSYS/MVITA code for 2 and 4 MPa internal pressure.


EXPERIMENTAL STUDY OF HEAT TRANSFER
IN THE SLOTTED CHANNELS AT CTF FACILITY

V. Asmolov, L. Kobzar, V. Nickulshin, V. Strizhov

1. INTRODUCTION

During core melt accident significant amount of core may relocate in the reactor pressure vessel lower head. During its cooling it may form cracks inside the corium and gap between corium and reactor vessel. Gap also may appear due to deformation of the lower head if its temperature exceed creep limit. Slotted channels ensure ingress of the cooling water into the corium, and exit of the generated steam. Study of the cool-down mechanism of the solid core debris in the lower head of the reactor vessel through gap and cracks is the objective of experimental work on the CTF facility.

Thermal hydraulics in the heated channels closed from the bottom and flooded with the saturated water from the top of the channel, is characterized by the counterflow of the steam and water, attended by such specific phenomena as the dry out when boiling, flooding and overturning of the coming down flow of water at the certain flow rates of the steam going up, partial dry out of the channel, and reflooding from the top of the heated channel with the saturated water. The above phenomena may reveal independently or in different combinations depending on geometric parameters of the channel, heat release, and coolant parameters. Interchange of these processes with a certain cyclic sequence is possible.

Experimental study was performed at the CTF (Coolability Test Facility) facility, which is a part of the thermohydraulic KC test facility in the RRC «Kurchatov Institute». Presented results are obtained at the CTF-1 test section which represents a vertical flat channel modeling a single crack in the solidified corium or the gap between the corium and reactor vessel.

The authors are grateful to the specialists of RRC «KI» V. Proklov, V. Kapustin, V. Zavalscky, A. Balyckin, V. Vinogradov, A. Khudyckin and others for preparation of the tests at CTF facility, processing experimental data and discussion of results. This work has been done with the support of the U.S. Nuclear Regulatory Commission managed by Dr. A. Behbahani, and his input to this work is highly appreciated.

2. EXPERIMENTAL EQUIPMENT

CTF-1 test section (fig. 2.1) is a vertical electrically heated flat channel. The channel is formed by two removable walls either or both can be heated. One of these walls (hereinafter called «2») was used in all of the described tests. The other wall «1» in some of the tests was replaced by the wall with a window for visual observations. In this case only one wall was heated.

Condenser, located in the top part of the tube above the test section, is used to remove heat.

In the case of high pressure, the channel is placed into a tight vessel. Additionally, pressurizer of KC facility is used to even the pressure in the channel and in the zone between the channel walls and pressurized vessel.
**Fig. 2.1.** The CTF test facility with the CTF-1 test section.

**Fig. 2.2.** The channel wall with spacer "pellets". View from coolant side.
The height of the walls is 400 mm (the heated height - 397 mm), width - 196 mm (heated width - 190 mm). Fixing of the walls at the necessary distance from each other (0.5-5 mm) is performed with the help of spacer cylinders «pellets» 10 mm in diameter.

At the top the slotted channel is smoothly transformed into the tube with the inner diameter of 80 mm. There is a pipeline at the bottom necessary for filling of the channel with the coolant, make-up and draining.

The electrical heater (fig. 2.2) is made of a steel plate (1 mm thick). There are cuttings (2 mm thick) in the plate, owing to which the current has the snaking motion. The heated height of the channel can be 397, 187, and 61 mm due to the use of the top current lead, and one of the three current leads located lower.

The heater is isolated from the metal wall by the talc-chlorite insulator (natural material) 39 mm thick.

Spacers are fixed in the holes drilled in the wall 2. «Pellets» and heaters are covered with electrically insulating layer (0.2 mm) of aluminum dioxide.

Instead of the wall 1 special wall with a round window of 120 mm in diameter made of the quartz glass is used for visual observations and video recording at ambient pressure. At high pressure observation is done through the second window in the pressurized vessel.

Data acquisition system allows to measure during the tests such parameters as voltage and current of the electric heating system, temperatures of the heaters (19-28 thermocouples for each heater), insulators (9 thermocouples in each wall), and the coolant (7 thermocouples), local actual void fraction (in 3 locations), pressure drops in different sections of the channel, weight level of the saturated water under the channel, parameters of the condenser secondary circuit, temperatures of the test section outer surfaces, temperature of the media in the inter-vessel space (when operating with the pressurized vessel), and the temperature of environment.

Data collection period of the one-time inquiry of n detectors can be identified by the expression:

\[ T = 0.00015 \cdot n + 0.001 \text{ sec.} \]

The collected information was put into PC.

3. RANGES OF THE STUDIED PARAMETERS

Study at the CTF-1 test section was performed for different combinations of regime parameters. The following denominations used to identify combinations of the varied parameters are presented below:

- D2/SS - channel with two heated walls - simulates the crack in corium;
- D2(J)/SS - channel of the previous design, but only one wall is heated (D1/SS - see below); J=1, 2 - number of the heated walls; simulating the gap between corium and reactor vessel;
- D1(J)/SS - channel with one heated wall; another wall has a window for visual observations and video recording;
- G - width of the gap between the walls of the channel (5, 2, 1, and 0.5 mm);
- H - heated height of the channel; adjusted by using different current leads (61, 187, and 397 mm);
- L - level of the saturated water above the channel (0.1, and 0.5 m).
4. EXPERIMENTAL RESULTS

4.1. Behavior of the Measured Parameters

Fig. 4.1 presents experimental data to illustrate the shape of the registered experimental parameters for the regime G2-D2(1)/SS-H397-L0.5 (2 mm gap between the walls, electric heating was applied to the heater of the wall 1 only, the heater is made of stainless steel, heating zone height - 397 mm, water level above the channel - 0.5 m, experiment was conducted under atmospheric pressure). In this test thermocouples were located in points 1-19 only (fig. 2.2). Variation of electric power and temperatures of the wall 1 heater versus time is presented.

Fig. 4.1. Time behavior of current power and heater temperatures on the heated wall No. 1 in the G2-D2(1)/SS-H397-L0.5 experiment.

At the beginning of the recording one can see improvement of the heat transfer and transition to the steady-state regime of the normal heat transfer due to power decrease. After that smooth increase of the heater electric power started, and the heat transfer is worsening again.

Periodic peaks up to ~150-230°C with the further return to the temperatures close to the saturation temperature are observed in the heated wall 1 in points 8 and 10 (thermocouples TH1-8 and TH1-10) located in the channel central axis at different elevations. This testifies to the fact that local worsening of heat transfer and "dry" spots with the increased temperature appear on the heater's surface, then these spots are wetted with the liquid and cooled down to the saturation temperature.

During approximately 3200-th second from the beginning of recording when the power was 3.42 kW (without consideration of heat losses) abrupt temperature growth up to ~300°C was observed in point 8; then the
temperature continued to grow in the pulsation regime. Temperatures in the other points of the heater of wall 1 (thermocouples TH1-4 and TH1-6 and others) were still close to the saturation temperature.

The time moment of the first registered temperature growth is considered as the beginning of the heat transfer worsening. In both this example and other tests, first local superheating of the wall under the unchanged power disappeared and became stable only after slight increase of the power.

Synchronic periodical increase and decrease of the heater temperatures in points 8 and 10 were established in the regime of the worsened heat transfer. Average temperature values were also approximately stable under the stable power. Power growth is necessary to increase the average temperature level.

4.2. Results of Visual Observations

Visual observations performed during experiments G2-D1(2)/SS-H397-L0.5 and G2-D1(2)/SS-H187-L0.5 (2 mm gap, only wall 2 is heated, heated height 397 and 187 mm, water level above the channel – 0.5 m, atmospheric pressure) showed the following:

- In the regimes of the worsened heat transfer, the major part of water flows from the top to the bottom along the side sections of the channel. These flows are more organized and stable when the heating height is 397 mm; in case the height is less (higher heat fluxes) the flow picture is less stable and is periodically spoiled with the water ingress from the flanges.
- Superheated sections of the walls in the center of the channel at temperatures of 400–600°C are cooled by the superheated steam coming from the bottom only.
- Two upper plates of the heater are never been superheated, this is ensured by the downward flow of the water located above the channel. Water penetration below the second plate of the heater was not observed in the regimes with large superheating.
- The influence of horizontal cuttings in the heated wall onto the processes is significant (the cuttings can simulate roughness of the surfaces in the corium cracks). They prevent heat transfer due to heat conductivity in the regimes with local superheating. It can not influence the value of the critical power when local superheating starts, but it influences significantly the broadening of the «hot» spot in the vertical direction. Besides, at low void fraction steam bubbles appear mainly in the cuttings, which play the role of the centers of steam generation.

4.3. Data on Critical Powers

Analysis of experimental data (table 4.1) allows to make the following preliminary conclusions:

- The greater the width of the gap between the walls, the higher the critical power is. The values of $W_{cr}$ are 3–6 times higher if the gap width is 5 mm, in comparison with the case of 1 mm gap.
- Critical powers increase along with the increase of pressure (fig. 4.2); in the tests with 1 mm gap the values of $W_{cr}$ increased approximately two times when the pressure increased.
Table 4.1  The critical power $W_{cr}$ [kW].
1) Test section design: D2/SS and D2(J)/SS.

<table>
<thead>
<tr>
<th>Gap $G$, mm</th>
<th>Channel height $H$, mm</th>
<th>Pressure $L$, bar</th>
<th>Variants of heating and water level</th>
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<th>D2(J)/SS</th>
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2) Test section design: D1(2)/SS.

<table>
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<tr>
<th>Gap G, mm</th>
<th>Channel height H, mm</th>
<th>Pressure bar</th>
<th>Variants of water level</th>
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<th>L = 0.1 m</th>
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<td>397</td>
<td>4.92, TH2-08</td>
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**Fig. 4.2.** Dependence of the Critical Power versus Pressure.

- From 5 to 80 bar; in the tests with 5 mm gap - 2-2.5 times when the pressure increased from 1 to 20 bar.
- If only one of the channel walls is heated $W_{cr}$ values are 30% lower in comparison with the case of two heated walls (heat fluxes are higher in case of only one heated wall).
- Dependence of $W_{cr}$ versus the heated height of the channel is ambiguous. In case of a 5 mm gap it increases along with the change of height from 187 to 397 mm by 15-50%. $W_{cr}$ does not practically depend upon the height of the channel in case of less gaps, i.e. critical heat fluxes grow along with the decrease of the heated height of the channel.
- Water level above the channel does not influence the critical power value in the reviewed range.
- In case of a 5 mm gap cyclic change of drying out of the channel section in its central axis is observed with the further flooding of this section and cooling it down up to the saturation temperature.
- Variation of values of the critical powers in case of repetition of the same test is ~30%.
5. CONCLUSIONS

- Maximum critical power reached in the performed tests was 43.24 kW (pressure - 2 MPa, gap width - 5 mm, channel height - 397 mm).
- Maximum critical heat flux reached in the tests was 1050 kW/m² (pressure - 2 MPa, gap width - 5 mm, channel height - 187 mm).
- The CHF data for the case with one heated wall are located above the data of Wallis correlation [1], but below the data of Zuber-Mond correlation [2, 3] (fig. 4.3, for 0.1 MPa).

![Graph](attachment:image.png)

**Fig. 4.3. Dependence of the Critical Heat Rate versus the Value of the Channel Height/Width ratio (for 0.1 MPa).**

- The limiting power of the channel under the temperature of ~600°C in the maximum hot area does not differ from the critical power in the majority of tests.
- Facility with the indirect heating of the wall has been prepared for testing for the gaps up to 15 mm and the whole range of pressures.

REFERENCES

Experimental Study on CHF in a Hemispherical Narrow Gap

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Abstract

As a part of the SONATA-IV program, KAERI is conducting an experimental investigation of critical heat flux(CHF) in hemispherical narrow gaps. A visualization experiment, VISU-II, was done as the first step to get a visual observation of the flow behaviour inside a hemispherical gap and to understand the CHF-triggering mechanism. It was observed that the counter-current flow limitation (CCFL) phenomenon prevented water from wetting the heater surface and induced CHF. The CHFG (Critical Heat Flux in Gap) test is now being performed to measure the CHF and to investigate the inherent cooling mechanism in hemispherical narrow gaps. Temperature measurements over the heater surface show that the two-phase flow behaviour inside the gaps could be quite different from the other usual CHF experiments. The measured CHF points are lower than the predictions by existing empirical correlations based on the data measured with small-scale horizontal plates and vertical annulus.

Introduction

During the TMI-2 accident, the reactor pressure vessel(RPV) survived, despite the fact that all severe accident analysis codes predicted it would fail. The gap cooling mechanism was suggested as a plausible one that could cool down the relocated corium.[1] There has been a lot of research related to the gap cooling concept. Some of it has focused on gap formation[2, 3] and some upon the heat transfer through the gap[4, 5]. For two years, the SONATA-IV(Simulation Of Naturally Arrested Thermal Attack In-Vessel) program has been being carried out at KAERI(Korea Atomic Energy Research Institute) to assess the cooling capability by that mechanism.

The visualization experiment, VISU-II, has been completed and the CHFG tests are now being conducted. The purpose of the CHFG experiments is to investigate the inherent cooling mechanism in a hemispherical narrow gap and to develop an empirical CHF correlation applicable to this geometry. In fact, some experimental and analytical CHF correlations related to flat or curved gaps are available in the literature but there are none applicable to the hemispherical narrow gap. The tests have been conducted with water, but tests with Freon-113 will follow in the near future. An experimental investigation of the cooling mechanism in hemispherical narrow gaps, focusing on CHF, is one of the major parts of the SONATA-IV program.

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Visualization Experiment: VISU-II

Kutateladze[6] thought of CHF as a hydrodynamic phenomenon and his theory is generally accepted. Therefore, understanding the hydrodynamic phenomena of boiling water in hemispherical narrow gaps is important in the study of CHF. We carried out experiments aiming to visualize boiling water inside a hemispherical gap and see the hydrodynamic phenomena triggering CHF. In order to provide visual observations, water and a hemispherical heater were placed in a transparent pyrex-glass vessel.

The VISU-II experimental facility consists of a hemispherical heater, power controller, a current/volt meter, a bell-jar shaped transparent vessel and a PC for data acquisition. There is also a mirror, lighting and a Hi-8 home video camera to visualize the flow inside the gap. We intended to make a 1 mm gap between the heater and pyrex-glass vessel itself. However, there exists some non-uniformity due to the difficulty in machining the pyrex-glass vessel. The top of the pyrex-glass vessel is open to the atmosphere. Figure 1 shows a cross-section detailing the test section, including the heater. An electric heater wire is located inside a hemispherical copper shell and the shell is filled with Wood’s metal of melting point 70°C. The thickness and outer diameter of the copper shell are 20 mm and 238 mm, respectively. The maximum heater power is 6 kW. Below the test section, a mirror slanting at 45° is installed to provide a visual observation of the test section bottom area.

Steam bubbles generated go upward alongside the hemispherical heater wall. At the same time, water goes down in the counter direction with the bubbles. The two phases flow violently in the gap and this flow pattern prevails from the bottom to the top end of the gap. Around the top end, steam tries to penetrate into the water pool above the heater while water flow into the gap. They flow in counter directions through separated flow paths. These flow paths are randomly established and disappear quickly. Figure 2(a) shows the multiple flow paths, the steam flow path is about 2-3 cm

Fig. 1 Cross-section of the Test Section

(a) Top area

(b) Bottom area

Fig. 2 Images of flow in the Gap

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wide. At a certain elevated heater power, the mass velocity of steam reaches a critical value corresponding to the CCFL. Jeong & No[7] named this type of CCFL entrance flooding because CCFL is initiated due to flow instability at the liquid entrance. That type of CCFL occurs when the liquid entrance geometry is sharp and the liquid flow rate is large enough. In this case, hydrodynamic and geometric conditions around the top end of the gap influence the CCFL significantly but those conditions of the gap below the top end do not. The CCFL is also affected by the gap size. In our facility, CCFL first occurs at the top-end of the gap where its size is small compared with the other part. According to observations, steam and water still flow actively through multiple paths in regions of larger gap sizes while CCFL occurs in regions of smaller gap sizes. The heater surface just below the region where CCFL occurs is locally dried out because CCFL prevents water from penetrating the gap. With the heater power that initiated CCFL, the local dryout region was small and often re-wetted by the water coming up from the bottom. Since the test section is not so big and the boiling two-phase flow fluctuated dynamically, water was able to reach the dryout region. With the further increase in heater power, however, the dryout region was enlarged and water could not reach it any more. Figure 2(b) shows the dryout region when the heater power is 5.5 kW. The left-hand-side is filled with water while the right-hand-side is dried out.

**CHFG Experimental Facility**

Figure 3 shows the CHFG experimental facility, which consists of an electric heater, a pressure vessel, a heat exchanger, a coolant control system and a coolant storage tank. An electric heater is put inside a hemispherical copper shell, which provides the maximum average heat flux of 90 kW/m² at the surface. The thickness and outer diameter of the copper shell are 25 and 500 mm, respectively. Four units of stainless steel pressure vessel were manufactured to provide gap sizes of 0.5, 1.0, 2.0 and 5.0 mm between the copper shell and the pressure vessel itself. The experiments are being performed using de-mineralized water and experiments with Freon-113 will be performed later. The

![Fig. 3 Schematic Diagram of CHFG facility](image1)

![Fig. 4 Thermocouple locations](image2)
CHF measurements will be made in the range of 1 to 10 atm. The heat generated by the electric heater is removed in a heat exchanger installed 150 cm above the top of the pressure vessel to maintain a near-saturated condition of the working liquid. The heat exchanger takes a role in system pressure regulation as well. A level gauge is installed in the pressure vessel to confirm that the heater is always covered with water during the experiments. The occurrence of CHF is noticed by 66 K-type thermocouple readings. The thermocouples are embedded in the copper shell, as shown in Fig. 4. From each pair of T/Cs, local heat flux is calculated and found to be within a ±20% variation of average in a nucleate boiling regime. The temperatures and mass flow rates are processed by a Hewlett Packard data acquisition system.

As the experimental facility constitutes a closed loop, the first step necessary to carry out experiments is to purge the air accumulated in the loop. If the air remains in the loop, it obstructs the heat transfer in the heat exchanger so that the working fluid might not circulate. Initially the heater power is maintained at a low level and the set-value of the pressure control system is set at a pre-determined value. All the temperature readings are displayed on a computer monitor and carefully observed. If the temperature readings are believed to reach a quasi-steady state, the heater power is increased step-wisely. When all the temperature readings increase monotonically without a limit, the heater power is cut off. Usually it took 15 to 30 minutes to reach a quasi-steady state in a low power range and more than 60 minutes near the CHF point.

**Results and Discussions**

Figures 5 & 6 show the temperature measurements made at a gap size of 2 mm with atmospheric pressure. Those are projections of the hemispherical surface of the copper shell. The boundary and center of the circular area refer to top end of the gap and the lowest bottom of the copper shell, respectively. Small circles shown in radial directions represent thermocouple locations. The readings from those thermocouples are interpolated to give isothermal lines. Figures 5(a)-(d) show temperature variation measured at heat fluxes of 32, 42, 52 and 60 kW/m², respectively. Through the present paper, heat flux refers to the average heat flux over the whole outer surface of the copper shell. Temperatures of the heater and copper shell reached a quasi-steady state after 15 ~ 30 minutes since the heater power changed. Quasi-steady state values were used to make these plots. The surface temperature was quite uniform up to a heat flux of 32 kW/m². At a heat flux of 42 kW/m², heat-up started from around the upper left edge. This high temperature region expands toward both the azimuthal and downward directions with an increase in heat flux. Considering the visual observations of VISU-II experiments, this indicates that a local dryout region expands with an increase in heat flux. The high temperature region always started from the upper left edge. The reason for this is speculated to be because the gap size around this area is smaller compared with the other region. Although the curved components of the facility were machined using a CNC machine, the gap size can be slightly non-uniform because of misalignment or thermal expansion. The measurements of actual gap size by the ultra-sonic technique(UT) under the same condition as the experiments showed that the deviation of gap size from the design value was within the instrument's inherent error. It can therefore be said that the gap size was reasonably uniform.
Fig. 5. Temperature variation
Gap size: 2mm
Avg. $q''$ (kW/m$^2$):
(a) 32 (b) 42 (c) 52 (d) 60
Elapsed time at $q'' = 68$ kW/m$^2$
(a) 0 (b) 500 (c) 1400 (d) 1600

Fig. 6. Self-expansion of dryout
Gap size: 2mm
Elapsed time at $q'' = 68$ kW/m$^2$
(a) 0 (b) 500 (c) 1400 (d) 1600

Fig. 7. Self-expansion of dryout
Gap size: 1mm
Elapsed time at $q'' = 60.3$ kW/m$^2$
(a) 0 (b) 1000 (c) 1500 (d) 2500
Figure 6 shows the temperature variation with time in seconds when the heat flux was fixed at 68 kW/m². Temperatures over the whole surface increase by itself, even though the heater power is maintained at a fixed level. This means there is no steady state at this heat flux, which is a different situation from that shown in Fig. 5. In the lower part of Fig. 6(a), there is a large area without isotherm lines. The temperature of this region remains slightly higher than 100°C and the area keeps shrinking with time. That is, the wetted region shrinks and the dryout region expands with time. The velocity of the dryout expansion and temperature increase rate of the dryout region get larger with time. This is because the local heat flux at the wetted region increases due to extra heat transferred from the dryout region by conduction. When the dryout region expands to the bottom of the copper shell, the temperature increase of that location was so fast that the heater power should be cut off immediately for heater protection. We defined this heat flux with which the dryout region undergoes self-expansion as the CHF in the geometry of hemispherical narrow gaps.

The present definition of CHF is different from those used in other experiments. If experiments are carried out with a specimen like a small plate, pipe and wire, the temperature jumps quickly as soon as the boiling regime turns into a stable film boiling because the heat capacity of the specimen is small. The heat flux at this situation is usually defined as the CHF. However, if the heat capacity of the heated section is large enough, such as the present facility (copper 200 kg), the temperature increases slowly because it needs a lot of heat to be heated up. Even in the local dryout region, the temperature increases slowly and it is limited. This is because the remaining heat which was not removed in the local dryout region, moves to the wetted region to be removed. The reason we do not define the occurrence of local dryout as CHF in the present experiments is as follows: (i) The present experimental facility does not allow the visual observation of the flow pattern, and it is therefore hard to tell whether the flow is in a stable film boiling regime only with temperature readings. (ii) It is hard to quantify the local gap size, even if the occurrence of local dryout is influenced by the gap size distribution. (iii) Even though local dryout occurs, all the generated heat finally cools down due to conduction, as mentioned before. Therefore the temperature does not increase monotonically but is limited by a certain value. However, above a certain heat flux, temperature increases continue. It is therefore reasonable in the present geometry that CHF should be defined as the heat flux which exceeds the maximum cooling capacity through a hemispherical gap so dryout region undergoes self-expansion and leads to a film boiling temperature.

Figure 7 shows the temperature variation when CHF occurs under atmospheric pressure and the gap size is 1 mm. The CHF occurred at the heat flux of 60.3 kW/m². The local dryout initiated at the same location as the case where the gap size is 2 mm. When the gap size was 1 mm, the dryout region expands faster in the azimuthal than downward direction. As can be seen in Fig. 7(c), the bottom surface is still wet although the top-end of the gap is dried off. In Fig. 7(d), the dryout region finally expands to the bottom and the temperature over the whole surface increases to over 200°C.

The predictions by Chang & Yao[8] and Monde et al.[9]’s CHF correlations are compared with the present results in Fig. 8. Chang & Yao[8] and Monde et al.[9] carried out CHF experiments with test sections of vertical annulus and vertical plates and developed the following correlations, respectively.

\[
\frac{q_{CHF}}{\rho_s h_L} = \frac{\rho_s^2}{g \sigma \rho} \left[ 1 + 6.7 \times 10^{-4} \left( \frac{\rho_s}{\rho} \right)^{0.8} (L/s) \right] \frac{0.16}{0.5} \]

(1)
\[
\frac{q_{\text{CHF}}}{\rho_k h_R} \sqrt{\frac{\rho_g}{gD\Delta\rho}} = \frac{0.38}{\left(1 + \sqrt{\frac{\rho_g}{\rho_l}}\right)} \cdot (L/s)
\]

where, \(q_{\text{CHF}}\) is the critical heat flux, \(g\) is the gravitational acceleration, \(\sigma\) is the surface tension, \(D\) is the diameter, \(\rho_l\) is the liquid density, \(\rho_g\) is the gas density, \(h_R\) is the latent heat of evaporation, \(L\) is the heated length and \(s\) is the gap size, respectively. For comparison, we put the parameters of the present experiments into the above correlations and the gap size was assumed to be 1 mm. Koizumi et al.[10]'s CCFL measurements are also compared. They carried out CCFL experiments in narrow-gap annular passages and reported their experimental data. The inner diameter of the outer pipe of the facility was 100 mm. Various sizes of the inner pipe were used to make the gaps of 0.5, 1.0, 2.0 and 5.0 mm. Since they did not suggest any CCFL correlation, we did some regression analysis to develop the following CCFL correlations:

\[
\frac{j_k^{*1/2}}{j_k} + 0.23\frac{j_i^{*1/2}}{j_i} = 0.32 \quad \text{for 2 mm gap} \tag{3}
\]

\[
\frac{j_k^{*1/2}}{j_k} + 0.35\frac{j_i^{*1/2}}{j_i} = 0.35 \quad \text{for 1 mm gap} \tag{4}
\]

where, \(j_k^* = j_k \cdot \sqrt{\frac{\rho_k}{gD_{eq}\Delta\rho}}\), \(D_{eq} = D_o - D_i\).

In order to present them in Fig.8, superficial velocities are changed into corresponding heat flux that can produce the same mass flow of steam. Koizumi et al.'s CCFL correlation seems to be close to the present measurements, while Chang & Yao and Monde et al.'s correlations predict much higher figures. The reason is thought to be that, in the present experiments, CCFL prevents water from penetrating into the top-end of the gap and CHF occurred at lower heat fluxes. Compared with those two empirical CHF correlations, the pressure effect of the present results seems to be quite small. As those two CHF correlations were developed based on the data measured under atmospheric pressure, the pressure trend predicted by them might not be correct. So the pressure effect needs further study. Figure 8 also shows that gap size effect on CHF in the present geometry is small when the gap size varies from 0.5 mm to 2 mm.

![Fig. 8. Comparison of measurements with correlations](image-url)
Concluding Remarks

CHF experiments in hemispherical narrow gaps and visualization experiments in the same geometry are in progress and have been done, respectively. According to visual observations, CCFL occurs at the top-end of the gap and prevents water from penetrating the gap. That is, it can be said that a CCFL bring about local dryout and finally, CHF in hemispherical narrow gaps. Even if local dryout occurs, there exists a quasi-steady state and the temperature of the dryout region is limited within a certain value. When the heater power is large enough, however, there is no quasi-steady state. The dryout region expands by itself without an increase in heater power and the temperature of the heater surface monotonically increases. That situation is defined as the CHF in the present experiments. The experiments using water were completed. Measured CHF values are much lower than the predictions made by empirical correlations applicable to flat plate gaps and annuli.

The maximum heat flux of the current heater is not big enough, and therefore CHF does not occur at an elevated pressure with large gap sizes. Since pressure effect on CHF needs further research, as mentioned before, we are going to carry out experiments using freon-113. The way to produce a high heat flux is being looked into as well. A scaling issue is also our concern. In order to be capable of addressing the issue, some separate effect tests are scheduled and in progress.

Acknowledgements

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Experiments on Heat Removal in a Gap between Debris Crust and RPV Wall

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Abstract

The thermal hydraulic phenomena which occur in a gap between a debris crust and the RPV wall was investigated experimentally. The goal of these experiments was to find out the limits of heat removal at the relevant system parameters. For simulation of the decay heat a maximum heat flux of 550 k$W/m^2$ was possible. The influence of gap width, water submersion, pressure and water subcooling on critical heat flux are presented.

Introduction

The consequences of serious accidents in light water reactors with partial core meltdowns are such that every conceivable step must be taken to ensure the integrity of the reactor pressure vessel (RPV). If the molten core material deposited in the lower plenum can be contained in the primary system, the effect of the accident can be mitigated much more easily than if it penetrates through into the containment, because the problems associated with the latter scenario such as concrete-melt interactions do not occur. The obvious conclusion, therefore, is that, even in the case of a beyond-design-basis accident, the consequences should be restricted to the primary system to the maximum extent possible.

The fact that realistic possibilities for this do exist has been demonstrated by the TMI-2 accident. According to information emanating from the TMI-2 Vessel Investigation Project (TMI-2/VIP), approx. 20 t of molten core material penetrated into the lower plenum (cf. 1/). As revealed by the samples taken, the RPV wall exhibited a thermal response profile during the course of the accident which, to date, the computational codes available have been unable to reproduce. Because a water reservoir was present in the lower plenum, it can be assumed that an efficient evaporative cooling process, which has not been included in the relevant numeric simulation techniques until now, prevented the reactor pressure vessel from failing. Thermal hydraulic calculations /2/, which were performed by the GRS (Society for Plant and Reactor Safety) using the ATHLET computer program, revealed that an oscillating water/steam flow is capable of performing the necessary heat removal function between the debris crust and the RPV wall. However, the question as to which conditions are conducive to the occurrence of such a flow pattern between the crust and the wall cannot be answered by simplified numeric simulation. Assistance has to be sought in experimental investigation performed with the objective of explaining and modeling the physical phenomena associated with the melt/wall interaction which occurred during the TMI accident. It was for this purpose that the "DEBRIS/RPV Wall Interaction" research project sponsored by the German Ministry of Education, Science Research and Technology (BMBF) was launched.

Within the framework of this project, thermal hydraulic phenomena which occur in a gap between the debris crust and RPV wall are being experimentally investigated at Siemens/KWU. The goal of these experiments is to find out the limits of heat removal within the full range of system pressure and heat flux corresponding to various essential parameters such as gap width and water submersion of the molten core material.
Figure 1: Mechanism of Heat Removal

Experimental Setup

In order realistically to simulate the processes which occur in the lower plenum during the cooling of the debris, it is first necessary to prepare a representative model for the debris cooling process. Assuming that a certain residual volume of water is always present in the lower plenum, there are in principle two conceivable scenarios by which cooling of the debris may take place:

- Slow cooling of the reactor pressure vessel wall during which a small quantity of water penetrates into the debris material via the pores and larger channels, heats up and subsequently is transported out of the debris again as a result of the density differential.
- An alternative cooling mechanism which enables rapid cooling of the reactor pressure vessel. Here, coolant flows both through the channels in the debris and also through a gap between the debris material and the pressure vessel wall. The fluid either totally or partially evaporates during this process, thus ensuring the necessary heat removal function (Fig. 1).

Rempe /3/ demonstrated that only the latter cooling mechanism is capable of absorbing and removing quantities of heat sufficient for cooling rates at the reactor pressure vessel wall of 10 to 100 K/min. The fact that a temperature-time gradient of this magnitude can occur in reality was revealed by the results of the investigations performed on material specimens of the reactor pressure vessel of TMI-2.

In order to simulate the cooling mechanism shown in Fig. 1, a test arrangement was selected as indicated in Fig. 2. An electrically heated spherical insert which serves as a mock-up of the solidified melt is located in a pressure vessel. The curvature of the lower plenum surface is shaped such that the angle of inclination of the heating surface at the outer edge is the same as the angle of inclination exhibited by the RPV lower head of TMI-2 at the edge of the solidified debris crust. This ensures that the conditions essential for the gravitational stratification effects are appropriately simulated.

The mock-up of the RPV wall is located below the heated spherical insert with a defined gap established between the two. It should be possible to remove the heat from the heated spherical insert through this gap without the occurrence of any overheating. As the gap width which occurs during the course of an accident is unknown, the distance between the heated spherical insert and the RPV wall mock-up is varied during the tests over a wide range.

In order to detect departure from nucleate boiling, the heated spherical insert is provided with a large number of thermocouples (Fig. 3). The RPV wall mock-up is also fitted with an - albeit smaller - number of temperature measuring points. This enables prompt detection of any dry-out of the gap between the heated spherical insert and the RPV wall mock-up, an occurrence which would cause overheating of the gap fluid. In addition, the fluid temperature in the vessel is measured at various positions. This enables registration of any thermal stratification which may occur.
Oscillatory processes in the gap produce pressure pulsations. In order to measure these, the pressure difference between a position at the center of the heating surface and another position located at the vessel wall was recorded. A further difference pressure sensor was used to register the liquid level in the vessel (and thus the height of the water coverage over the heating surface).

The pressure vessel is connected at the inlet with the heating facility of the BENSON test rig installed at Siemens/KWU, while the medium flowing out of the vessel (water, two-phase mixture or steam) is fed to a spray condenser (cf. Fig. 4).

The BENSON test rig is characterized by the following main specifications:
- System pressure up to 330 bar
- Temperature up to 600 °C
- Mass flow up to 28 kg/s
- Heating capacity up to 2 MW

Thanks to this wide range of parameter values, the operating conditions of diverse heat transfer systems can be accurately simulated.

Figure 2: Test Apparatus
Figure 3: Arrangement of Temperature Measuring Points

Figure 4: BENSON Test Rig with Test Vessel

Test Matrix and Test Performance

The test matrix is oriented in the first instance to the conditions which occurred during the TMI-2 accident. However, in order to obtain results and data which are as generally applicable as possible, all the parameters are varied over a wide range.

The system pressure in the case of the TMI-2 accident hovered around 100 bar at the time of hot-spot formation at the pressure vessel wall (cf. /4/). However, because core meltdown could also occur at any other pressure level, parameters are also assigned to the system pressure (Table 1). As regards the heat flux at the heating surface, attainment of the value which occurred during the TMI-2 accident of approx.
300 kW/m² is regarded as a minimum requirement. In order to also simulate larger melt masses, which consequently also give off larger quantities of heat, the heat flux is to be raised to a maximum of approx. 550 kW/m². The remaining parameters, such as water coverage of the crust (water submersion), fluid subcooling in the test vessel, and gap width between crust and wall, have been selected so that the influence of these variables on heat transfer behavior can be effectively defined (for details, see Table 1).

In order to determine the maximum possible heat removal rate, the heat flux at the heating surface is gradually increased with the remaining parameters held constant. In the event that departure from nucleate boiling occurs, a phenomenon which is detected from the sudden increase in the heating surface temperature, the heating energy supply is shut down and the maximum achievable heat flux is recorded accordingly.

<table>
<thead>
<tr>
<th>Pressure</th>
<th>Unit</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Heat flux</td>
<td>kW/m²</td>
<td>max. 550</td>
</tr>
<tr>
<td>Water submersion</td>
<td>m</td>
<td>0.5, 1.3, completely filled</td>
</tr>
<tr>
<td>Water subcooling in the test vessel</td>
<td>K</td>
<td>0, 10</td>
</tr>
<tr>
<td>Gap width</td>
<td>mm</td>
<td>1, 3, 5, 10</td>
</tr>
</tbody>
</table>

Table 1: Test Matrix

Results

It is recognized that as the power increases, the time behavior of the heating surface temperature differs greatly from known forced convection experiments. Whereas, for example, the heating surface temperatures with a heated pipe flow experience only small fluctuations up to the occurrence of the boiling crisis, very large temperature variations could be observed with the gap cooling tests. These variations start at a certain swell value for the heating surface load that rises with increasing gap width. If the heating surface is dried out in a short period of time, it becomes wetted again after a few seconds. The reason for this can be seen in the transient gap flow which occurs with cyclical pulsations. When the heating surface can no longer be rewetted, the material temperature increases rapidly and the boiling crisis is reached. Figure 5 shows clearly the described event. Temperature fluctuations of more than 20 K occur with a gap width of 3 mm before the boiling crisis is reached. At 10 mm and with the same thermal loadings accrues an almost constant heating surface temperature.

The pressure difference measurement taken between a position at the center of the heating surface and a position located at the vessel wall clearly shows the occurrence of pulsations in the gap (Figure 6). In this case the parameters are the same as for the top part of Figure 5. The variation of the pressure difference measured is in agreement with the difference in hydrostatic pressure drop between a water and steam filled gap. This can be explained by the several periods (incubation, expulsion and refill) of a geysering cycle. With approximately 25 to 30 s the periods of the pressure pulsations are comparatively long and they correlate with the time interval between two cyclic dryouts shown in Figure 5.

It is expected that the occurrence of the boiling crisis is subjected to statistical fluctuations. This is due to the examined gap cooling which is not a flow with a pre-defined direction on the basis of a superimposed pressure difference. The effect is shown in Figure 7 where temperature peaks occur at different positions. In the left hand part one marked temperature peak represents a local dryout, whereas on the right hand part two marked peaks can be seen.
The primary interest for the development of a heat transfer correlation is the transferable heating power, which is possible with the help of the gap cooling, as a function of the forementioned parameters. As shown in Figure 8, heat flux densities of more than 150 kW/m² are transferred with a gap width of 1 mm and a pressure of 10 bar. The figure also shows that with a pressure increase, the critical heat flux increases. This behaviour would be also expected, if the influence of the pressure is similar to that of pool boiling /6/. Widening the gap width increases the critical heat flux density. By a gap width of 5 mm between heating element and RPV model, the boiling crisis is no longer attainable with the available heating capacity.

The values for the critical heat flux lay somewhat lower if the water in the test vessel reaches the saturation state (Figure 9). The heat flow rate (around 300kW/m² at 100 bar), which were produced during the
TMI-2 accident are also reached with a gap width of 1 mm. Furthermore, the experiments with saturated water in the test vessel show that the influence of the water coverage over the heating surface on the critical heat flux density is omittable. The minimum value of water coverage of this experiment amounted to 0.5 m.

Figure 6: Pressure Fluctuations in the Gap at High Heat Flux

Test 6 111 134
Gap width: 1 mm
Pressure: 10 bar
Subcooling: 10 K
Heat flux: 170.2 kW/m²

Test 6 110 321
Gap width: 3 mm
Pressure: 100 bar
Subcooling: 0 K
Heat flux: 494.2 kW/m²

Figure 7: Occurrence of Boiling Crisis at the Heating Surface
Figure 8: Critical Heat Flux for Subcooled Water Pool

Figure 9: Critical Heat Flux for Saturated Water Pool

Summary and Conclusions

The experiments determining the critical heat flux density in a narrow gap have shown that relatively high heat fluxes are realised with small gap widths. This conforms to a water gap of 1 mm with the decay heat released by the TMI-2 accident.

The results which are obtained from the scaled down model are to be transferred to the full scale. With help of computer models, which are based on the extracted physical principles, it is then possible to calculate the behavior of the original components.
Note

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Experimental Investigation of Creep Behavior of Reactor Vessel Lower Head

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1. INTRODUCTION AND BACKGROUND

The objective of the USNRC supported Lower Head Failure (LHF) Experiment Program at Sandia National Laboratories is to experimentally investigate and characterize the failure of the reactor pressure vessel (RPV) lower head due to the thermal and pressure loads of a severe accident. The experimental program is complemented by a modeling program focused on the development of a constitutive formulation for use in standard finite element structure mechanics codes. The problem is of importance because:

• lower head failure defines the initial conditions of all ex-vessel events;
• the inability of state-of-the-art models to simulate the result of the TMI-II accident (Stickler, et al. 1993); and
• TMI-II results suggest the possibility of in-vessel cooling, and creep deformation may be a precursor to water ingestion leading to in-vessel cooling.

2. SCALING ANALYSIS AND EXPERIMENTAL DESIGN

A scaling analysis was performed (Chu et al., 1997) to ensure sub-scale experiments are properly designed. The key results are:

• the experimental apparatus should be geometrically scaled from a reactor pressure vessel to preserve the hoop stress;
• prototypical material should be used because with present knowledge, material creep behavior can not be scaled;
• the heat flux for the experiment should be scaled by the experiment scale to preserve the temperature history.

3. EXPERIMENTAL APPARATUS

The apparatus is basically a scaled version of the lower part of a TMI-like reactor pressure vessel (RPV) without the vessel skirt, consisting of a SA533B1 steel hemispherical head, and a 30-cm vertical section replicating the lower part of the RPV cylindrical wall, see Figures 1. The inner-diameter of the lower head is 0.91 m corresponding to a geometrical scale factor of 4.85. The wall thickness is typically 30 mm. Due to the hot-spinning forming operation, the wall is slightly thicker at the equator.

A hemispherical heater with nine independently controlled segments is used to simulate the energy transfer from the core debris to the reactor vessel, see Figures 1 and 2. The inner surface of the lower head is coated with Pyromark® black paint for efficient radiation absorption. The outer surface of the lower head and the inner surfaces of the cylindrical section and the top flange are insulated. A cooling band near the bottom of the cylindrical section provides the proper far field temperature condition (due to the presence of water in the RPV). The pressure load is provided by a manifold of bottled argon, and controlled by automatic fill and bleed valves.

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Initially, the vessel is filled to approximately half of the desired pressure, and the vessel pressure increases with heating. When the vessel reaches 800 K, the vessel pressure is set at the desired testing pressure and is maintained at that pressure while the vessel is heated to failure.

The shape and the local wall thickness of the vessel are measured before and after the experiment using a grid system defined by punch marks on the vessel surface. A computerized mapping device, based on the tracking of a manually positioned pointer, was used in mapping the vessel shape. An ultrasonic thickness gage was used for vessel wall thickness measurements. Locations on the hemisphere are described in terms of “longitude” and “latitude.” The equator of the bottom head is 0° latitude and the bottom center of the lower head is 90° latitude.

Arrays of thermocouples are used to measure inner and outer wall temperatures. Linear displacement transducers deployed along a chosen longitude, at typically 3-5 latitude locations, are used to monitor the deformation of the test vessel. Except for the bottom center of the lower head (90°), there are two transducers at each location, one for vertical displacement, and one for horizontal displacement. X-ray is also used to monitor real-time profile of the test vessel.

4. EXPERIMENTAL RESULTS

The results of the first five experiments out of the planned eight-experiment series are reported here. The main variables are heat flux distribution, vessel pressure, and the absence or presence of penetrations on the vessel bottom. All five experiments were performed with an internal pressure of 10 MPa corresponding to a hoop stress of 75 MPa in the vessel wall.

4.1 LHF-1 Experiment – Uniform Heating and No Penetrations

The lower 60° (latitude 30° to 90°) of the vessel wall was heated uniformly in LHF-1. Representative temperature and displacement histories of the vessel wall are shown in Figure 3. Significant increase of the creep rate was observed at about 120 minutes into the test at a vessel temperature of approximately 930 K. As the wall temperature increased, the creep rate continued to accelerate. The vessel failed catastrophically at 145 minutes into the test; at a vessel temperature of 1011 K. The recorded displacement at vessel bottom center at the time of failure was 0.12 m.

As shown in Figure 4, the vessel failed non-symmetrically with respect to the 90°/270° plane. The 180° longitude (left side) profile shows more deformation than the 0° profile. The deformation is relatively symmetrical with respect to the 0°/180° plane. The shape of the failure is approximately oval, measuring 0.49 m by 0.25 m. The hole opening corresponds to the material bounded approximately by latitude 66° and 80°, and longitude 110° and 240°. Post-test inspection indicates that the initial failure occurs at approximately 150° longitude/66° latitude.

Examination of the pre-test and post test thickness maps of the vessel, Figures 5 and 6, suggests that the failure region corresponds to a region of slightly reduced wall thickness in the pre-test vessel, and the thin section “attracts” deformation during vessel creep.
The overall extension of the vessel was approximately 16-cm, corresponding to an overall strain (based on the vessel outer radius) of 33%. The linear strain near the failure location was found to be about 200% (distance between punch grid marks increased by a factor of 3), corresponding nicely with the 9(3°) fold wall thickness reduction, from 29 mm to 3 mm. The wall thickness along the edge of the failure varies from 3 mm to 16.5 mm. Based on these values, the linear strain at failure varies from approximately 34% to 200%.

4.2 LHF-2 Experiment - Center-Peaked Heat Flux and No Penetrations

LHF-2 has a center-peaked heat flux profile reminiscent of TMI-II. The initial LHF-2 heating schedule followed the same temperature history of LHF-1 up to 700K. Beyond 700K, the center region of the vessel (68°-90°) was controlled to follow LHF-1, and the surrounding region was maintained to be approximately 100K lower.

LHF-2 was completed in two runs due to heater failure. The experiment was interrupted approximately 180 minutes into the first run of the experiment with the bottom center section (68° to 90°) at approximately 1000K and experienced 7 cm of deformation.

Representative temperature and displacement histories of the vessel wall during the second run are shown in Figure 7; the displacements shown are values beyond that of the first run (7 cm at 90° and 5 cm at 70°). Figure 8 illustrates the center-peaked vessel temperature profile and the corresponding vessel yield strength distribution. The yield strength is based on a best fit of existing property values as a function of temperature (Pilch et al., 1998). Significant creep occurred at approximately 135 minutes and failure occurred at approximately 160 minutes into the second run. The corresponding wall temperature at creep initiation was in the range of 930-950 K. The vessel temperature at failure was in the range of 1000-1025 K.

The overall profile of the posttest vessel, as shown in Figures 9 and 10, was shaped like an inverted top half of a pear. The total vessel deformation was 16.7 cm. There is an inflexion point in the 60°-70° region corresponding to the knee in the vessel yield strength profile, Figure 8. The failure is an oval approximately 4 cm by 7 cm, substantially smaller than the 25 cm by 49 cm of the LHF-1 failure. The minimum wall thickness at failure was approximately 3 mm, similar to LHF-1. The failure appeared to have initiated near 77° latitude and 205° longitude, and is bounded between 77° to 79° latitude and 200° to 210° longitude.

4.3 LHF-3 Experiment - Edge-Peaked Heat Flux and No Penetration

LHF-3 seeks to simulate the edge-peaked heat flux distribution due to coremelt convection. The initial heating schedule for LHF-3 was designed to follow essentially the same temperature history of LHF-1 up to approximately 800K; beyond this temperature, heater segments were adjusted to achieve an edged-peaked heat flux distribution centered around 34° latitude. The 34° location is the approximate coremelt level corresponding to 75% of the core of a typical PWR.

Representative temperature and displacement histories of the vessel wall

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are shown in Figure 11. Figure 12 illustrates the edge-peaked vessel temperature profile and the corresponding vessel yield strength distribution. Significant creep occurred at approximately 140 minutes at a peak vessel temperature of 960K and failure occurred at approximately 160 minutes at a peak wall temperature of approximately 1000K. The measured deformation at failure, at the bottom center, was about 5 cm. The failure of LHF-3 is a nearly perfect latitudinal rip at 33.5°, coinciding with the location of the temperature peak, see Figure 13. The rip spans approximately 70° between 310° and 20° longitude. From the deformation of the grid pattern it is also quite obvious that the region of large strain is confined to materials surrounding the rip. An examination of the pre-test vessel thickness mapping indicated a region of reduced thickness between 250° and 360° longitude for the 30° to 40° latitude region of the vessel. There is a 50° overlap between the thin-wall region (250°–360°) and the failure region (310°–20°). It is interesting to note that the circumferential distribution of the peak temperature is uniform within 10K, but the low temperature/high strength region is near the 270° longitude. It is plausible that pre-test vessel wall thickness variation is still the precursor of vessel failure but the final failure configuration is modified by the 10K circumferential temperature variation.

4.4 LHF-4 Experiment - Uniform Heating with Penetrations

The purpose of the experiment was to examine the effect of penetrations on vessel failure. The vessel was uniformly heated. The penetration pattern was an exact scaled duplicate of the penetration pattern of a typical PWR in the region of 60° to 90° latitude of the lower head, see Figure 15. The range of angular location overlaps the expected region of vessel creep for a uniformly head lower head, based on the observations of LHF-1. The scaled Inconel penetration tube has a diameter of 8.2 mm. The largest diametrical clearance between a penetration tube and its through-hole is 0.2 mm (0.007 mils). The tube-to-wall weld only penetrates 7.6 mm (0.3 in) into the vessel wall, approximately 1/4 the total wall thickness.

The temperature and displacement histories of LHF-4 are shown in Figure 16. The heating history of the vessel was designed to be comparable to LHF-1. Due to a failure of heater zone 3, the actual heating rate was slightly slower than that of LHF-1. Creep initiation was observed 140 min into the experiment at a vessel temperature of approximately 930K. This temperature is consistent with the creep initiation temperature observed in LHF-1. The vessel developed a leak at 190 min into the experiment, at a vessel temperature between 970K (at 68° latitude) and 980K (at 90° latitude). The total deformation at the time of vessel failure was approximately 4 cm or about 8% strain. This is substantially smaller than the 12 cm, 25% strain observed in LHF-1.

Post-test examination indicated the failure occurred at penetration #9, located at 73° latitude, see Figure 17. The through-hole for the penetration was greatly enlarged, from 8.2 mm to a 1.3 mm by 1.6 mm oval. The neighboring penetrations at 68° latitude and 78° latitude also show similar through-hole deformation. It is interesting to note that these angles overlap the LHF-1 failure zone (66° to 80°). A comparison of the pre-test and post-test appearance of the weld indicates that failure occurred at the weld-fillet/vessel interface, see Figure 18. Apparently, the large global deformation of the vessel simply pulled apart the weld. Since Inconel has higher strength at elevated temperature, it is reasonable
that failure occurred at the weld fillet/vessel interface.

4.5 LHF-5 Experiment - Edge-Peaked Heat Flux with Penetrations

The LHF-5 experiment is designed to investigate the effect of penetrations on vessel failure with an edge-peaked heat flux distribution. There were nine scaled penetrations in the vessel bottom between latitudes 41° (highest penetration location for a PWR pressure vessel) and 80°. The selected installation locations represent the latitude range of penetration in a typical PWR. The installation also reproduces the maximum number of neighboring penetrations (3) to examine the possibility of the "postage rip effect" in a realistic geometry.

LHF-5 made use of an induction heated graphite radiating cavity located within the test vessel, see Figure 19. The cavity consists of a "top hat" supported on a 12.7 mm graphite base plate using 16, 8.9 cm high, 25-mm-diameter graphite spacers. The cylindrical section of the top hat is heated by induction. Thermal radiation escaped through the 8.9 cm gap between the rim of the top hat and the base plate is used achieve the edge-peak heat flux profile on the lower head.

The temperature, displacement, and pressure histories of LHF-5 are shown in Figure 20. The peak temperature is at 37° latitude. The peak temperature contrast for LHF-5 is in the range of 200K; therefore, the normalized (with respect to yield stress at temperature peak) yield stress profile has a sharper minimum as compared to LHF-3, Figure 21. It appears that the test apparatus developed a slight leak at approximately 120 minutes into the experiment resulting in a gradual decrease in internal pressure from 9 MPa to 7.7 MPa at about 165 minutes. The pressure was brought up to the desired pressure of 10 MPa within 1.1 minutes. This increase in pressure resulted in a slight but noticeable (∼1 mm) response in the displacement transducer. Judging from the displacement measurement at 90°, vessel creep initiated at or slightly after the vessel pressure was increased to 10 MPa; the corresponding vessel temperature was 940K at 0° longitude and 1000K at 180° longitude. After creep initiation, the vessel continued to deform at an accelerated rate as the peak vessel temperature increased. The vessel ruptured at approximately 200 minutes into the experiment. The vessel temperature at failure was in the range of 1030K-1120K, with the maximum vessel temperature at 180° longitude. The overall vertical deformation at the time of failure was approximately 4 cm, comparable to the 5 cm observed in LHF-3.

As shown in Figures 22 and 23, the vessel failed with a horizontal rip at 37° latitude. The location of the rip appears to be coincidental with the circular locus of the peak temperature at 37° latitude. Post-test inspection indicated that vessel failure initiated at approximately 200° longitude quite near the region of maximum vessel temperature. The failure propagated in both directions for approximately 160° to 165°. Only the 5° to 40° section remained attached. The vessel thickness at the region of initial failure was between 3 mm and 4 mm, and increased monotonically along the tear, reaching approximately 11 mm near the termination of the tear. Both penetrations at 41° latitude (closest penetrations to the peak temperature location) suffered weld failure, see Figure 24. However, the penetrations were sufficiently far from the failure site, they were unlikely to have contributed to the failure.
4.6 Comparisons Between LHF-3 and LHF-5 Experiments

LHF-3 and LHF-5 both had side-peaked heat flux distribution. LHF-3 vessel failed with a partial rip and LHF-5 vessel was almost completely severed. The key difference is the relative magnitudes of hoop stress and the vessel yield stress at the time of creep initiation. The nominal hoop stress in the vessel wall for 10 MPa internal pressure is 75 MPa. At creep initiation LHF-3 was in the range of 960-970K; the corresponding yield stress is 120-127 MPa. In LHF-5, because of the delay in full pressurization, at creep initiation, the vessel temperature was in the range of 980-1025K, the corresponding yield stress was in the range of 70-110 MPa.

Perhaps it is reasonable to speculate that because of the sensitivity of creep to temperature:
- Delaying the initiation of creep (or initiating creep by pressurization) until the vessel was well above previously observed temperature for creep initiation may have contributed to the nearly complete ripping of the bottom head.

5. SUMMARY of OBSERVATIONS from LHF-1 to LHF-5 EXPERIMENTS

- Localized heating results in localized failure.
- The failure size is typically smaller than the heated region.
- Temperatures for the initiation of failure and final failure appears to be fairly consistent.
- The wall thickness at failure appears to be fairly constant.
- It appears that the failure location might be related to slight variations in the manufacturing of the vessel.
- Vessel with penetration can fail prematurely as a result of weld failure due to the large strain associated with global deformation of the vessel.
- Pressure transients can have significant effects on vessel failure.

ACKNOWLEDGMENTS

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Figure 1. Schematic of LHF test apparatus.

Figure 2. An overall view of the LHF test vessel and support.

Figure 3. LHF-1 temperature and displacement history.

Figure 4. LHF-1 vessel profile 270° view - 180°/0° plane.

Figure 5. LHF-1 Pre-test thickness map (by latitudinal angle).

Figure 6. LHF-1 Post-test thickness map (by latitudinal angle).
Figure 7. LHF-2 temperature and
  displacement history.

Figure 8. LHF-2 vessel temperature
  and strength profile.

Figure 9. LHF-2 Vessel profile,
  180° view - 90°/270° plane.

Figure 10. LHF-2 vessel
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Figure 11. LHF-3 Temperature and
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  longitudinal angle).

Figure 12. LHF-3 Yield strength
  Profile at 150 minutes.
Figure 13. Close-up view of LHF-3 failure.

Figure 14. LHF-3 wall temperature history at four circumferential locations near the peak temperature region.

Figure 15. LHF-4 penetration pattern.

Figure 16. LHF-4 temperature and displacement history.

Figure 17. LHF-4 penetration through-hole enlargement.

Figure 18. LHF-4 penetration weld (post-test).
Figure 19. LHF-5 experimental design.

Figure 20. LHF-5 vessel temperature (180° azimuth; 30°, 37°, and 45° latitude), internal pressure, and displacement history.

Figure 21. Comparison of latitudinal yield stress profile for LHF-3 and LHF-5.

Figure 22. LHF-5 post-test view.

Figure 23. LHF-5 post-test view.

Figure 24. Penetration weld failure.
LOWER HEAD THERMO-MECHANICAL BEHAVIOUR

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1 - INTRODUCTION

In the case of a severe accident, the corium could fall in the hemispherical vessel lower head and generate a thermal shock. The lower head thickness will be submitted to a thermal gradient through its thickness. Two models will be proposed to estimate the creep rupture of the lower head depending on the flooding of the reactor cavity.

If the reactor pit is not flooded, the thermal gradient in the thickness is not important and the temperature increases rapidly : the first model is based on the creep law at average temperature of the shell thickness. In fact. The failure occurs when the creep damage is unallowable or the membrane stress reaches the ultimate strength.

In the case of the reactor pit is flooded, it is necessary to take into account the relaxation of stresses due to internal pressure in the wall thickness which is in the creep regime. The stresses decrease versus time, but the stresses in the wall thickness which are in the elastic and plastic regime increase to fulfil the equilibrium equations. The failure occurs either when the membrane stress in the elastic and plastic thickness vessel head reaches the ultimate strength or when the creep damage is unallowable.

2 - THE FIRST MODEL

2.1 A BRIEF DESCRIPTION OF THE MODEL

The elastic and plastic rate are small and they are neglected when the lower head is in creep regime. The creep rate is important at the beginning of the creep regime, and then it decreases to reach a constant value versus time. This evolution can be written that :

$$\dot{\varepsilon}^e = A(T) \sigma^{(e(T))} f(t)$$  \hspace{1cm} (1)

or \( \sigma(T) f(t) \)  \hspace{0.5cm} \left( \frac{\dot{\varepsilon}^e}{A(T)} \right) = \left( \frac{1}{n^e(T)} \right) $$  \hspace{1cm} (2)

The equilibrium equations allow to establish a relation between the equivalent membrane stress versus the stress distribution in the thickness :

$$e \bar{\sigma}(f(t)) = \int_{-\frac{\alpha}{2}}^{\frac{\alpha}{2}} \sigma(x) f(t) \frac{1}{n^e(T)} \ dx$$  \hspace{1cm} (3)

By performing finite element thermal calculations, we have noticed that the thermal gradient is not far from a linear distribution. The previous equation becomes with the variable change \( dz = \frac{\text{d}z}{\Delta T} \text{d}T \) :

$$\bar{\sigma}(f(t)) = \int_{\text{e}^\text{ref}}^{\text{e}^\text{ref}} \sigma(T) f(t) \frac{1}{n^e(T)} \Delta T \text{d}T$$  \hspace{1cm} (4)

Integrating the last equation gives the equivalent membrane stress versus the creep strain.
\[
\bar{\sigma} = \frac{1}{\Delta T} \frac{1}{K} \dot{\varepsilon} \lambda^{\frac{\Delta T}{2}} \left( \frac{K \Delta T}{2} \right)
\]

\[K = a \log \dot{\varepsilon} + c \log \lambda\]  

\[
\left( \frac{1}{A(T)} \right)^{\frac{1}{n(T)}} = \lambda^{c + \frac{\Delta T}{2}}; \frac{1}{n(T)} = aT + b
\]

A simplification of the previous formula can be performed when \(a \log \dot{\varepsilon} < c \log \lambda\) which give \(K \approx c \log \lambda\) and finally:

\[
\dot{\varepsilon} = \left( \frac{c (\log \lambda) \Delta T}{\text{sh} (c (\log \lambda) \Delta T/2)} \right)^{1/b} (f(t))
\]

2.2 APPLICATIONS

The material properties are those of a ferritic steel SA533B (NUREG/CR-5642 [1992]) excepted for the creep law (DOE - ID/104 60).

Plastic behavior

Ultimate Strength : \(\sigma_u(T) = 275 \left(1 + \cos \pi \frac{T - 300}{1200}\right)\) with \(\sigma_u(T)\) in MPa, \(T\) in Kelvin.

Creep law

\(\varepsilon = A \sigma^t \) with \(\sigma\) in MPa and \(t\) in hours and the ductility is equal to 10%.

<table>
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<tr>
<th>T(K)</th>
<th>A</th>
<th>r</th>
<th>s</th>
</tr>
</thead>
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<tr>
<td>673</td>
<td>4.05 (10^{-2})</td>
<td>7</td>
<td>0.243</td>
</tr>
<tr>
<td>773</td>
<td>2.21 (10^{13})</td>
<td>4</td>
<td>0.4</td>
</tr>
<tr>
<td>903</td>
<td>3.05 (10^{9})</td>
<td>3</td>
<td>0.85</td>
</tr>
<tr>
<td>1003</td>
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<td>1.77</td>
<td>0.7</td>
</tr>
<tr>
<td>1373</td>
<td>2.5 (10^{4})</td>
<td>4.74</td>
<td>0.714</td>
</tr>
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</table>

One application is presented with the hypothesis of dry reactor cavity and no water in the vessel. 1 m³ of corium oxyde is considered to be relocated two hours after the emergency shutdown of a PWR 900 MWe; it corresponds approximately to an heat flux equal to 0.12 MW/m². The temperature distribution in shell thickness is estimated with finite element code (the liquidus temperature is considered at 1600 K and the external heat exchange coefficient equal to 50 W/m²K). The relative pressure is supposed constant in the present study ranging from 0.1 MPa to the maximal value 16.6 MPa. The ablation is initiated before the mechanical failure only for pressures lower than 0.5 MPa. The residual thickness when the failure occurs is 0.1m for 0.2 MPa and 0.06 m for 0.1 MPa (the initial thickness of the lower head is equal to 0.13 m). For pressure higher than 0.5 MPa, the failure is only a mechanical failure by creep or plastic deformation with no contribution of the ablation.
Figure 1 presents the membrane stress and the ultimate strength versus the pressure when the failure occurs. The plastic failure is predominant only when the pressure is greater than 10 MPa.

Figure 2 presents the creep time to rupture versus the pressure. It is ranging from 1.5 hours to 3.2 hours respectively for 16.6 MPa to 0.1 MPa. The effect of the pressure (factor to 166) on the time to rupture is small; it is due to the rapid rise of the temperature in the thickness. Then, figure 3 presents the average temperature of the wall; we can notice that for low pressure the temperature value is close to the liquidus temperature of ferritic steel.

3 - THE SECOND MODEL

3.1 GENERAL PRESENTATION

If the reactor cavity is flooded, the temperature drop through the thickness is constant and equal to the difference between the melting temperature of steel (1600 K) and by the boiling temperature of the water (373 K). Then, there will be a partial ablation of the shell thickness in order to accommodate the thermal heat flux. The creep rate is governed by the membrane stress due to the internal pressure, the mean temperature and the thermal gradient through the thickness. Nevertheless, due to the thermal gradient, only a part of the wall thickness is only under creep regime and the associated stresses relax as the time increases. And so, this thickness under creep regime does not participate to the structural integrity of the vessel wall. Then, the stresses which are in the other part of the wall increase versus time to allow in equilibrium with the external load (in this study, the internal pressure).

One can define three zones in the wall thickness: an elastic zone, a plastic zone and a visco-elastic zone. The first one which corresponds to the elastic material behavior, lays between the external surface of vessel and plastic radius \( R_{\text{plastic}} \). The plastic radius is defined when the equivalent stress reaches the yield stress. The second zone extends from the plastic radius to the creep radius, \( R_{\text{creep}} \), for which the temperature of the vessel is equal to the creep temperature, \( T_{\text{creep}} \), corresponding to the significant creep. Then, the third zone is defined between the creep radius and the internal radius of the vessel wall.

An extensive presentation of the model will be given at the ASME PVP exhibit 1998 (C. STRUB and B. AUTRUSSON [1998]).

3.2 - A BRIEF DESCRIPTION OF THE MODEL

The `elastic part` \( (r \geq R_{\text{plastic}}) \)

The displacement purely radial of a sphere submitted to an internal pressure is modelled with that equation:

\[
\varepsilon(r, t) = \frac{A(t)}{3} \left( r + \frac{3K}{4\mu} \frac{R_{\text{ext}}^2}{r^2} \right)
\]

\( K \) and \( \mu \) are the Lamé parameters, \( K = \frac{E}{3(1-2\nu)} \) et \( \mu = \frac{E}{2(1+\nu)} \).

The radial stress and the equivalent Von Mises stress are equal to:

\[
\sigma_r(r, t) = A(t)K \left( 1 - \left( \frac{R_{\text{ext}}}{r} \right)^3 \right) \quad \text{and} \quad \sigma^e(r, t) = \frac{3}{2} A(t)K \left( \frac{R_{\text{ext}}}{r} \right)^3
\]
The elastoplastic part \( \left( R_{\text{creep}} < r < R_{\text{plastic}} \right) \)

The plastic displacement also purely radial is:

\[
u^{\prime}(r, t) = \frac{1}{3K} \left( r\sigma_r + \frac{B(t)}{r^2} \right)
\]  
(10)

At the point on the plastic radius, the equivalent elastic stress is equal to the yield stress and the elastic displacement is also equal to the plastic displacement. Consequently, we can obtain a solution of the parameters \( B(t) \):

\[
B(t) = \frac{1 - \nu}{1 - 2\nu} R_{\text{plastic}}^3 \sigma_y
\]  
(11)

The visco-elastic part \( \left( r \leq R_{\text{creep}} \right) \)

The creep strain rate is deduced from deviatoric stress tensor and the equivalent stress (Von-Mises):

\[
\dot{\varepsilon}^{\text{cr}} = \frac{3}{2} \frac{\cdot S}{\sigma}
\]  
(12)

where \( \rho = \sqrt{\frac{1}{3} \dot{\varepsilon}^{\text{cr}} \dot{\varepsilon}^{\text{cr}}} \) represents the cumulated creep strain rate.

Due to stress tensor equations and the equivalent stress, the creep strain rate tensor can be written as follows:

\[
\begin{align*}
\dot{\varepsilon}_r & = -\rho, \\
\dot{\varepsilon}_\theta & = \dot{\varepsilon}_\varphi = \frac{\rho}{2}
\end{align*}
\]  
(13)

Model developpement

The stress state follows the global equilibrium equation (in the three zones):

\[
\int_{R_{\text{creep}}}^{R_{\text{plastic}}} \frac{2}{r} \sigma dr + \int_{R_{\text{creep}}}^{R_{\text{plastic}}} \frac{2}{r} \sigma_y (r) dr + \int_{R_{\text{plastic}}}^{R_{\text{perm}}} \frac{2}{r} \sigma dr - p_{\text{c}} = 0
\]  
(14)

In the visco-elastic part of the thickness, the elastic strain is small with regard to the creep strain, \( \varepsilon^{\text{cr}} = 0 \), and the creep strain tends to zero when the shell temperature tends to \( T_{\text{creep}} \) (unsignificant creep).

With the above equations from the paragraph entitled « visco-elastic part », we can write:

\[
\frac{\partial \rho}{\partial t} + \frac{3}{r} \frac{\rho}{r} = 0
\]  
(15)
The solution of the previous equation is:
\[
\begin{align*}
\dot{\rho} &= \frac{\rho_0}{r^3} \\
\rho_0 &= \text{constant}
\end{align*}
\] (16)

In introducing the creep law \( \dot{\varepsilon}^c = A(T)\varepsilon^{n(r)} \), we obtain:
\[
\dot{\rho} = \frac{\rho_0}{r^3} = A(T)\varepsilon^{n(r)}
\] (17)

\[
\dot{\sigma} = \left( \frac{\rho_0}{A(T) \cdot r^3} \right)^{\frac{1}{n(r)}}
\] (18)

The equilibrium equation is \( \frac{\partial \sigma}{\partial r} = \frac{2}{r} \cdot \sigma \) and \( r = R_{ext} \) : \( \sigma_r = -P_{cp} \) (primary circuit pressure), we obtain the equation of the radial stress in the creep part of vessel thickness, \( T \geq T_{creep} \Rightarrow R_{min} \leq r \leq R_{creep} \):
\[
\sigma_r = \int_{R_{min}}^{R_{creep}} 2 \left( \frac{\rho_0}{A(r) \cdot r^3} \right)^{\frac{1}{n(r)}} \! dr - P_{cp}
\] (19)

The equation of the equivalent stress in the elastic zone is \( R_{plast} \leq r \leq R_{ext} \) is:
\[
\dot{\sigma} = -\frac{3}{2} \frac{R_{ext}^3}{(R_{ext}^3 - R_{plast}^3)} \sigma_r (R_{plast}) = \sigma_y (R_{plast})
\] (20)

At \( r = R_{ext} \), we write the continuity of the equivalent stress:
\[
\dot{\sigma} = -\frac{3}{2} \frac{R_{ext}^3}{(R_{ext}^3 - R_{plast}^3)} \sigma_r (R_{plast}) = \sigma_y (R_{plast})
\] (21)

The general equation:
\[
\int_{R_{min}}^{R_{creep}} 2 \left( \frac{\rho_0}{A(r) \cdot r^3} \right)^{\frac{1}{n(r)}} \! dr + \int_{R_{min}}^{R_{plast}} 2 \sigma(r) dr + \int_{R_{plast}}^{R_{creep}} \left[ 1 - \left( \frac{R_{plast}}{R_{ext}} \right)^3 \right] \! \sigma_y (R_{plast}) - P_{cp} = 0
\] (22)

The constant \( \rho_0 \) is obtained from the equality between the elastoplastic and the visco-elastic stresses at the absissa \( R_{plast} \). The distribution of stresses through the thickness is estimated solving this equation with a computer code.

3.3 APPLICATIONS

The material properties are those of a ferritic steel SA533B (NUREG/CR-5642 [1992])

**Creep Behavior**

Creep rate : \( \varepsilon_{creep} = \alpha_1 (T) \sigma^{\alpha_2 (r)} \) with \( \varepsilon_{creep} \) in seconds\(^{-1}\), \( T \) in Kelvin and \( \sigma \) in MPa
with: \( \alpha_1(T) = 10^{\frac{17.2}{T-25}} \) and \( \alpha_2(T) = \frac{T}{150} - 3 \)

The creep becomes significant when the temperature reaches 800 K and the ductility is equal to 10% (the present version of the model does not allow to use a primary creep law).

First of all, a thermal calculation has been done to estimate the temperature distribution in the vessel wall. The water temperature in the reactor cavity is considered at 373 K (saturated value for one atmosphere). The liquidus temperature of the steel at 1600 K. Assuming a nucleate boiling regime, a high value of the heat exchange coefficient is considered equal to 10000 W/(m²K). According the SULTAN tests (Rouge, NURETH 7), the corresponding critical heat flux could be between 0.3 and 0.6 MW/m². The figure 4 presents the residual thickness versus the heat flux.

The mechanical estimations have been done when the thermal steady state regime is obtained. For each heat flux, a time to rupture versus the internal pressure value can be determined. On figure 5, the time to the rupture varies from 1 hour to some thousands hours. The time to rupture decreases drastically as the heat flux increases, but the creep rupture could be avoided if the internal pressure is less than 8 MPa and the vessel is flooded no more after 100 hours the emergency shutdown.

4. - CONCLUSIONS

Two mechanical models devoted to creep failure have been presented and some results are given concerning a typical PWR vessel.

The first model is simplified but sufficient to predict the failure if the vessel and the reactor pit are dry. 1 m² of corium oxide is considered relocated two hours after the emergency shutdown of a PWR 900 MWe ; it corresponds approximately to a heat flux of 0.12 MW/m². The pressure is supposed constant in the present study ranging from 0.1 MPa to the maximal value 16.6 MPa. The failure is not the complete ablation of the vessel thickness. The failure occurs in plastic regime for pressure greater than 10 MPa or in creep regime for pressure lower than this value.

If the reactor cavity is flooded, it is necessary to account for the relaxation of stresses due to internal pressure in the wall thickness which is in the creep regime. The stresses decrease versus time, but the stresses in the wall thickness which are in the elastic and plastic regime increase to meet the equilibrium equations.

Assuming a nucleate boiling regime, a high value of the heat exchange coefficient is considered equal to 10000 W/(m²K). According the SULTAN tests, the corresponding critical heat flux could be between 0.3 and 0.6 MW/m². The higher the heat flux is, the lower the time to rupture is, but the creep rupture could be avoided if the internal pressure is lower than 8 MPa and the vessel is flooded no more than 100 hours after the emergency shutdown.

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Figure 1

Dry pit - Plastic failure

Stress in MPa

Pressure in MPa

Stress in shell in MPa

Strength limit in MPa
figure 2

Dry pit - Creep rupture

figure 3

Dry Cavity - Average temperature of the shell

figure 4

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Pressure Vessel Creep Rupture Analysis

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1. INTRODUCTION

The vessel structural integrity is one of the key issues in the analysis of possibility of the in-vessel melt retention [1]. The system pressure and thermal hydraulic behavior of the relocated molten materials determine loads, stresses and displacements of the reactor vessel and finally failure mode and time to rupture. Analysis of PWR vessel lower head has been performed in papers [2,3]. Comprehensive analysis of LWR lower head behavior has been performed in the report [4], where high temperature data of carbon steel used in the most US LWR are presented. A systematic analysis of in-vessel melt retention problem including pressure vessel response to the expected thermal loads has been performed in the paper [5]. Recently several experiments have been conducted at SANDIA National Laboratories on creep rupture of a pressurized reactor lower head scaled model (1:5) [6]. In the tests pressure was about 10 MPa and temperature was about 1000 K. Different thermal loads were simulated by resistive heating of the inner surface of the hemisphere.

For the analysis of RPV structural integrity finite element codes are usually used for predictions of vessel failure. Data obtained in the SNL LHF experiments can be used for validation of models. The objective of this paper is to simulate LHF-1 and 2 experiments by the developed HEFEST code [7] utilizing finite element approach. Results of FEM analysis of these experiments are supplemented with the studies of uncertainties of code predictions with the simplified approach realized in the LOHEY code. It was found that the main source of uncertainties in the analysis is high temperature material properties data. Results of analysis are compared with the experimental results. Previously the LOHEY code has been benchmarked [8] and used for TMI-2 accident simulation [9].

2. STRUCTURAL MODELS OF LOWER HEAD DEFORMATION BEHAVIOR

Two-dimensional finite element (FEM) code HEFEST [7] allows for computational analysis of different problems. It includes heat conductivity module and nonlinear structural analysis module. Typically, the analysis involves two step procedure. In the first step temperature distribution in the structure is determined by solving the heat transfer problem with appropriate initial and boundary conditions. At the second step deformations in the structure are calculated using nonlinear models for material properties.

In accordance with different sources of stresses constitutive relations for total strain increment may be written as (applying the small strain theory)

$$\Delta\varepsilon = \Delta\varepsilon^e + \Delta\varepsilon^th + \Delta\varepsilon^pl$$

where $\Delta\varepsilon^e$ - elastic strain, $\Delta\varepsilon^th$ - deformations due to temperature gradients, $\Delta\varepsilon^pl$ - plastic deformations.

In consideration of creep rupture expression for $\Delta\varepsilon^cr$ as a plastic deformation $\Delta\varepsilon^pl$. Time independent plasticity is treated as in [10] using the von Mises function and the associated flow rule (4) assuming isotropic hardening for yield stress:

$$\sigma_y = \sigma_y^0 + He^p$$

Yield stress $\sigma_y$ can also depend on strain rate. The time-dependent plastic creep strain is modeled by applying the commonly used Norton's law for strain rate $\psi$ in the form:

$$\psi = 3/2A\sigma_{y}^{m-1}s_{ij}$$

where $\sigma_y$ is stress intensity, $s_{ij}$ is the strain deviator.

The material constants $A$ and $m$ were obtained from uniaxial experiments [4] for temperature ranges of 900-1373 K. In Table 1 evaluated from experiments creep coefficients are presented. In the temperature interval between 900 and 1050K temperature dependencies are very strong. For calculations $A$ and $m$ were presented as a function of temperature.

<table>
<thead>
<tr>
<th>T, K</th>
<th>A</th>
<th>m</th>
</tr>
</thead>
<tbody>
<tr>
<td>900</td>
<td>2.2 \times 10^{-26}</td>
<td>3.6</td>
</tr>
<tr>
<td>1000</td>
<td>0.75 \times 10^{-20}</td>
<td>1.8</td>
</tr>
<tr>
<td>1050</td>
<td>4.1 \times 10^{-24}</td>
<td>4.2</td>
</tr>
</tbody>
</table>

Table 1. Creep coefficients (SI units)
For simplified model realized in the LOHEY code the following main assumptions are used:

- the half spherical or half ellipsoidal lower head is considered as a set of symmetric about vessel axis hoop elements deformed independently (see Figure 1);
- the following loads acts in hoop element: temperature loads, meridional and hoop normal forces caused by internal pressure and weight loads;
- the hoop element is considered as a multi-layer shell; in each layer temperature along radial, hoop and meridional direction is assumed to be constant;
- the layer stress state is assumed biaxial; radial stress is not considered, only two diagonal components of stress tensor are taken into account.

![Figure 1. Lower head geometry.](image)

The basic equations which allow to describe the lower head stress-strain state evolution with account of these assumptions can be written by the following way.

The hoop and meridian layer strain in each layer may be written as:

\[
\varepsilon_i(t) = \varepsilon_i^{el}(t) + \varepsilon_i^{pl}(t) + \varepsilon_i^{cr}(t) + \varepsilon_i^{th}(t),
\]

\[i = m, \theta\]

where \(\varepsilon_i(t), \varepsilon_i^{el}(t), \varepsilon_i^{pl}(t), \varepsilon_i^{cr}(t), \varepsilon_i^{th}(t)\) - meridional \((i = m)\) and hoop \((i = \theta)\) strains: total, elastic, plastic, creep and thermal; \(t\) - time.

Elastic and thermal strain are written as:

\[
\varepsilon_i^{el}(t) = S_i \sigma_i(t);
\]

\[
\varepsilon_i^{th}(t) = \alpha_i^{th} \Delta T(t),
\]

where \(S_i\) - the components of the tensor of elastic properties; \(\alpha_i^{th}, \Delta T\) are the thermal expansion coefficient and the temperature increment; \(\sigma_i(t)\) - stress components.

Plastic strain and creep strains are written as:

\[
\varepsilon_i^{pl}(t) = \int_{0}^{t} d\varepsilon_i^{pl}(\tau), \quad (\alpha = pl, cr ; \quad i = m, \theta),
\]

\[
de_i^{cr} = \frac{3}{2} \frac{d\sigma_{int}^{pl}}{\sigma_{int}} (\sigma_i - \sigma_0),
\]

where \(d\sigma_{int}^{pl}\) is the intensity increment of \(\alpha\)-th strain component (plastic or creep strain); \(\sigma_{int}\) - the stress intensity; \(\sigma_0 = (\sigma_m + \sigma_\theta) / 3\).

The intensity increment of plastic strain is calculated with the help of \(\varepsilon_i^{pl} - \sigma_{int}\) diagram by the following way:

\[
de_i^{pl} = \frac{K_{load}}{H} d\sigma_{int},
\]

where \(K_{load}\) - the loading condition coefficient: neutral loading \((d\sigma_{int} = 0)\) and unloading conditions \((d\sigma_{int} < 0)\) correspond to \(K_{load} = 0\), loading conditions \((d\sigma_{int} > 0)\) corresponds to \(K_{load} = 1\); \(H\) - the tangent plastic modulus according to \(\varepsilon_i^{pl} - \sigma_{int}\) diagram.

The correlation for the creep strain intensity increment may be written as:

\[
de_i^{cr} = A \sigma_{int}^{m}(t) e^{-\frac{Q}{RT(t)}} dt
\]

where \(A, m, Q\) - the creep constants; \(dt\) - the time increment.

Using the plain section assumption it is possible to write 2N-2 correlations:

\[
e_i, n R_i^n = e_{i, n+1} R_i^{n+1},
\]

\[i = m, \theta ; n = 1, ..., N - 1\]

where \(R_i^n\) - meridional \((i = m)\) and hoop \((i = \theta)\) radii of n-th layer.

After substitution (1) - (3) into (6) we have the system of the 2N-2 equations in 2N unknown stress components \(\sigma_i, n\) \((i = m, \theta)\). The two additional equation are obtained from the consideration of the force balance:

\[
\sum_{n=1}^{N} \sigma_i, n(t) \delta_n(t) = T_i, \quad (i = m, \theta)
\]
Table 2. Creep rate of SA533B1 steel

<table>
<thead>
<tr>
<th>T, K</th>
<th>P, MPa</th>
<th>max. elongation, %</th>
<th>min. creep rate, %/h</th>
<th>Time to tertiary creep, h</th>
<th>Time to creep rupture, h</th>
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<tr>
<td>900</td>
<td>70</td>
<td>22.4</td>
<td>0.03</td>
<td>70</td>
<td>190.1</td>
</tr>
<tr>
<td>900</td>
<td>140</td>
<td>24.3</td>
<td>0.35</td>
<td>5.27</td>
<td>11.3</td>
</tr>
<tr>
<td>1000</td>
<td>56</td>
<td>37</td>
<td>2.13</td>
<td>2.58</td>
<td>4.6</td>
</tr>
</tbody>
</table>

where \( T_i \) - meridional \((i = m)\) and hoop \((i = \theta)\) normal forces; \( \delta_n(t) \) - the thickness of \( n \)-th layer. It is assumed that thickness \( \delta_n(t) \) changes are due to unelastic deformation.

Meridional and hoop normal forces equilibrate the external loads - vessel pressure and weight of corium and vessel wall.

**Pressure loads.** The normal forces in half ellipsoidal lower head caused by vessel overpressure are equal:

\[
T^\text{press}_m = \frac{PR_\theta}{2} \quad T^\text{press}_\theta = \frac{P R_\theta}{2} \left( \frac{2-R_\theta}{R_m} \right),
\]

where \( P \) is the in-vessel pressure.

**Weight loads.** Let's consider the lower head in section with polar angle \( \varphi \). It is assumed that corium is liquid and top level of corium is characterized by polar angle \( \varphi_{\text{cor}} \). If considered section is placed under the corium top level, normal forces are caused by 1) hydrostatic pressure \( P_{\text{hydr}} \) of liquid corium, placed above the considered section, and 2) by weight \( W(\varphi) \) of vessel wall and corium, placed under the considered section. The normal forces in a lower head wall caused by hydrostatic pressure of liquid corium is estimated with help of (8), where

\[
P = P_{\text{hydr}} = \rho_{\text{cor}} g z,
\]

where \( \rho_{\text{cor}} \) - the corium density; \( g \) - the acceleration of free fall; \( z \) is the height of corium placed above the considered section.

If to suggest that hoop force caused by the weight of corium and vessel wall is negligible then we receive the correlation for normal meridional force:

\[
2\pi R(\varphi) T^\text{weight}_m = W(\varphi) \sin \theta;
\]

Because described approach considers only normal forces and it does not consider the bending component of loads (and the shear force), caused by meridional gradient of physical parameters, we take into account only meridional force \( T^\text{weight}_m \) as source of stresses into vessel wall.

So, the normal forces in the force balance equation (6) are equal:

\[
T_m = T^\text{press}_m + T^\text{hydro}_m + T^\text{weight}_m
\]

\[
T_\theta = T^\text{press}_\theta + T^\text{hydro}_\theta
\]

**Failure criteria.** Two failure criteria are included in the considered models. According to the first criterion the layer fails if the plastic strain intensity exceeds the ultimate failure strain. The second criterion is based on the analysis of material damage induced by creep strains. Using the correlation for time to rupture at given stress and temperature, the current damage of each layer is determined. A maximum cumulative damage reaches the certain limit rupture is assumed. Lower head global rupture occurs when all layers of the hoop element are melted or failed.

### 3. SIMULATION OF LHF-1 AND LHF-2 EXPERIMENTS

A 1:4.85-th scaled experiment to characterize rupture failure of reactor vessel lower head due to thermal and pressure load has been performed at SNL. The hemispherical RPV model made of SA533B1 steel, 0.91 m in diameter and thickness of 29.8 mm - 33.3 mm, which uniformly varied from bottom center to equator was tested. Two experiments were considered with different thermal loads: uniformly heated wall in the test LHF-1 and bottom peaked heating in the test LHF-2.

Vessel pressure and vessel temperature time histories for LHF-1 experiment are shown in Figure 4 and Figure 5, correspondingly. Experimental rupture time was 2.40 hour.

The calculated axial displacement time histories obtained with FEM HEPESF code at the vessel bottom is plotted in Figure 6 together with experimental data. Two cases were considered: a) thermal elastic plastic model with hardening without creep, and b) full material model which includes creep. One can see that in both cases good agreement with experiment was obtained, namely:

- up to \( t = 125 \text{ min} \) calculated strain is equal to measured values for both models;
both models predict time of significant increase of the strain rate which corresponds to experimental data.

At 145-th min deformation achieved 30% which is in a good agreement to the 33% in the test.

For uncertainty and sensitivity analysis, the method of statistical tests has been used with the LOHEY code. The following mechanical properties were assumed as random parameters: the yield strength, the ultimate strength, the creep rate, the time to the creep rupture, the ultimate plastic strain. The probability density of the vessel failure for LHF-1 experiment is shown in Figure 5. Predicted failure time was 2.55 hour (exceeds the experimental value by 6%).

A specific feature of the LHF-2 experiment was the two stage loading of the model. Vessel pressure and vessel temperature time histories are shown in Figure 8 and Figure 9, correspondingly. As in the LHF-1 test, the intensive deformation was observed at nearly constant pressure of 10 MPa. Temperature in the bottom center was about 1000 K during first stage. Because of malfunctions in heating system and leak, the vessel was depressurized and experiment was interrupt.

During the second stage at 475 minutes nominal pressure of 10 MPa was reached when temperature in a bottom center was 900 K. Vessel failure was observed at 510 minutes when maximum temperature was 1020 K. The maximum strain reached the value of 35%.

Only first stage of the LHF-2 test was simulated with HEFEST code. During time 135-225 min the axial and radial displacements were increasing with approximately constant rate. Calculated deformation of vessel at 225 minutes is plotted in Figure 11. As for LHF-1 two cases were considered with and without accounting for creep modeling.

Up to the time instant of $t_1 = 125$ min, strain rate gradually increased for both cases presented in Figure 10. At this time stress intensity in steel reached the value of von Mises criterion, indicating the beginning of plastic deformation. Further increase in strains is associated with the steel creep behavior. Elastic-plastic model predicts the same deformation rate up to 200 minutes, which increase abruptly with the pressure increase from 10 to 12 MPa at $t=200$ min. Calculated yield stress $\sigma_y$ (980K) was 15% greater then nominal. Corresponding stress intensity $\sigma_T=80$ MPa. The discrepancy between calculational and experimental $\sigma_y$ was within the uncertainty range. In calculations with full model, creep law coefficient was $m(T)=1.8$ as in [4] but $A(T)$ varied from $10^{-22}$ at 900K, to $2\cdot10^{-20}$ at temperature of 1000K. For this choice of creep constant agreement with experimental data was reasonable. Because during the first stage of loading, a significant degree of deformation was achieved, the second phase was not modelled because residual stress and annealing might change those properties.

Calculations with LOHEY module predicted that failure time of vessel model was 8.53 hours which is in a good agreement with experimental rupture time of 8.50 hour. We assumed that the internal pressure and temperature after 8.5 hours were constant and equal to the values corresponding failure conditions. The probability and the probability density of the vessel failure in LHF-2 experiment are shown in Figure 13.

As it was mentioned the calculations of LHF-2 experiment were sensitive to the choice of creep constants. At 125-130 minutes intensive deformation of the vessel was observed. At this time maximum temperature in the test vessel reached 950 K. In the temperature range between 900 - 1000K limited amount of experimental creep data are available. Creep data from the report [4] are presented in Figure 12.

There are different possible approaches for evaluation of correlation parameters, for example time to creep rupture in our example. For the description of rupture time the following expression is usually used:

$$t^* = B \sigma^m G e^{-T},$$

(12)

where $B, m, G$ are the material constants. The results presented in the report determine time to rupture using linear part which is characterised by constant strain rate (See Figure 3). Therefore, the use of this data will allow to predict time to rupture but deformations will be smaller.
Figure 2. Young modulus and yield stress for SA533B1 steel

Figure 3. Sketch of creep strain time history

Figure 4. Vessel pressure time history during LHF-1 experiment.

Figure 5. Vessel temperature time history during LHF-1 experiment and density of the probability

Figure 6. Axial displacement of the lowest vessel point for LHF-1 test

Figure 7. Calculated deformation of pressure vessel for LHF-1 experiment at 145 min
Figure 8. Vessel pressure time history during LHF-2 experiment.

Figure 9. Vessel temperature time history during LHF-2 experiment for the different locations measured from the bottom center.

Figure 10. Axial displacement of the lowest vessel point for LHF-2 test.

Figure 11. Calculated deformation of pressure vessel for LHF-2 experiment at 145 min.

Figure 12. Approximations of the experimental point on the time to creep failure.

Figure 13. Probability and a density of the probability vs. time during LHF-2 experiment.
If we assume that in the formula (12) $m$ does not depend on temperature than the correlation presented in Figure 12, should have the same slope in coordinates $(\ln(t) - \ln(\sigma))$ for different temperatures. This line is presented in figure as a solid line. Otherwise, if we use data obtained for each temperature independently, results will differ significantly as it is shown in Figure 12 by dashed line.

Simulation of the test with the data presented by solid lines resulted in the vessel failure during the first stage because estimated time to creep rupture is about 0.6-0.8 hours. If parameters are chosen in accordance with dashed lines, time to creep rupture equals to two hours and vessel failure does not occur during the first stage.

4. SUMMARY

In the LHF-1 uniformly heated test the failure time and deformations predicted by FEM code are in a reasonable agreement to the test data. Both elasto-plastic and creep models can be applied for analysis. In the LHF-2 test only creep model predicts reasonably test data. The reason for difference is in the differences of heating. Heated area in the second test was smaller due to bottom peaked heating, which led to the reduction of the area of intensive deformation and as a consequence to the reduction of effective stress. This reduction was sufficient for stabilization of plastic state, and the character of deformation had more pronounced creep nature.

It was found in the simulations with both models that results of LHF-2 experiments were very sensitive to accepted material properties. In particular, both creep strain rate and time to creep rupture depend on the choice of temperature dependencies of parameters. Use of formulas (5) and (12) with pre-exponential constant which did not depend on temperature, did not allow to get reasonable agreement with test data.

5. REFERENCES


Parametric Studies on Creep Behavior of a Reactor Pressure Vessel Lower Head

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CSNI-Workshop on In-vessel Core Debris Retention and Coolability,

Abstract

Objective of our work is to develop methods to estimate the failure time of the reactor pressure vessel (RPV) lower head due to core melt scenarios and to quantify safety margins against failure. For this task adequate material properties are needed to characterize the material behavior especially the creep behavior at temperatures up to 1300 °C. Best estimate hot spot loading conditions of the Three Miles Island (TMI) RPV lower head with a wall thickness of about 130 mm have been analyzed concerning creep behavior. A parametric study was performed in which hypothetical test vessels with wall thickness down to 40 mm were analyzed under adequate loading conditions. The results show that after about 76 minutes the deformation in the center of the hot spot region of the thin shell is about 20 times larger than in the thick shell. Therefore the resistance against creep failure is much stronger in a thick shell because of the thermal gradients in the wall which prohibits significant creep deformation. This study shows that parametric studies of numerical calculations can be very effective for the design of experiments.

Analysis Model

For the lower head of a TMI like reactor pressure vessel an axisymmetric finite element model (see Fig. 1) was used to simulate the structure mechanical response due to a best estimate thermo-mechanical loading assumption of the core melt szenario described in the following chapter. Furthermore models of hypothetical test vessels with wall thickness of 100 mm, 70 mm and 40 mm were analysed. The calculations were performed with the structure mechanics analysis chain based on the nonlinear Finite Element program ADINA [1].

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Loading Assumptions

The loading assumptions due to the TMI core melt scenario are based on metallurgical investigations of the RPV lower head. Therefore a hot spot region with diameter of about 1 m and temperatures up to 1100 °C at the inner surface appeared. In the analysis the hot spot region was assumed in the center of the lower head. Figure 2 shows the assumed time history of the temperature distribution at the inner surface of the lower head. Outside the hot spot a debris region with temperature 600°C and a water region with 315 °C was assumed. Furthermore the time history of the internal pressure based on measurements during the accident is shown in Figure 2. For the hypothetical vessels with reduced wall thickness the pressure was scaled according to the wall thickness to simulate the same mean primary load in the vessel wall. In the analysis first the vessels are loaded by constant pressure (9.7 MPa in case of RPV wall thickness 130 mm) and temperature 315 °C for artificial 10000 s (167 min). Then the assumed loading scenario due to core melt started.

Material Model

In the analysis a nonlinear elasto plastic material model with creep was used for the RPV steel SA 533 Grade B Class I. In [2] the creep behaviour of the steel dependent on temperature and stress was characterized by the Bailey-Norton formula

\[ \varepsilon_{\text{creep}}(T, \sigma) = a_0 \sigma^{a_1} t^{a_2} \]

with \( a_{0,1,2}(T) \) given in Table 1.

For the temperatures 800 °C and 1000 °C the time history of this creep strain approximations are illustrated for different stress levels (see: Figures 3 and 4). Temperature dependent material properties were considered in the analyses. For simplicity in this parametric study the physical properties of the German RPV steel 22 NiMoCr 37 (special melt) were used (see Table 2).

Results of the Parametric Study on the Influence of the Wall Thickness

For the characterization of the vessels behavior especially in the first 76 min of the core melt scenario selected analysis results are shown in Figures 5-12. Figures 5 and 6 show that in the case with wall thickness 130 mm significant temperature gradients are build up in the vessel wall. In the case with 40 mm wall thickness there is nearly no temperature gradient in the wall. After 10 minutes the temperatures in the wall
reach about 700 °C at the inner surface. Locally the effective stresses (Figs. 7-8) reach the yield stress which decreases with temperature according to Table 2. After 76 min the outer surface temperature reaches about 800 °C in case 130 mm and about 1 000 °C in case 40 mm. The effective stress distributions reached in the vessels after 76 min in the transient are similar with stress level of about 30 MPa at the outer surface and about 15 MPa at the inner surface. The temperature and stress distributions in the vessels effect the creep strain distributions shown in Figure 9 and 10 which show strong differences mainly due to the different temperature distributions. The maximum creep strains are much larger in the case of small wall thickness and are reached in both cases at the outer surface. This effects that the deformation of the vessel with wall thickness 40 mm is much larger than in the case with 130 mm (see Figure 11 and 12). After about 76 min the ratio is about 20. Therefore failure of the thin vessel is expected much earlier compared with the thick vessel.

Conclusions

The documented parametric study demonstrates the strong effect of the wall thickness on creep behavior of sherical shells like the RPV lower head under a TMI like core melt szenario. It shows that creep failure in a scaled thin shell is reached much earlier than in a RPV lower head because, without additional loading conditions like external cooling, significant temperature gradients can be build up only in a thick shell.

Work on improved approximations for the creep behavior including the tertiary region was started. This will give a good basis for the development of rupture criteria for creep failure which will be verified by calculations of available experimental results.

Acknowledgment

The GRS work was financially supported by the German Minister for Education, Science, Research and Development (BMBF).

References

Tables and Figures

Table 1: Creep coefficients according to $\varepsilon_{\text{creep}} (T, \sigma) = a_0 \cdot \sigma^{a_1} \cdot t^{a_2}$ with $a_{0,1,2} (T)$ (from [2])

<table>
<thead>
<tr>
<th>T [°C]</th>
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<th>$a_1$</th>
<th>$a_2$</th>
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</thead>
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</tr>
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</table>

Table 2: Physical properties of the RPV steel 22NiMoCr 3 7 used in the analyses

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<tr>
<th>T [°C]</th>
<th>E [GPa]</th>
<th>$\nu$</th>
<th>$R_{p,0.2}$ [MPa]</th>
<th>$\varepsilon_{\text{ultimate}}$ [%]</th>
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Figure 1: Axisymmetric Finite Element model of the TMI like RPV lower head

Figure 2: Time history of the pressure and the temperature loading (center of hot spot)
Figure 3: Creep curve approximations used in the parametric study for the RPV steel SA 533 Grade B Class I at 800 °C according to [2]

Figure 4: Creep curve approximations used in the parametric study for the RPV steel SA 533 Grade B Class I at 1000 °C according to [2]
Figure 5: Temperature distribution in the vessel wall (wall thickness 130 mm) for different times.

Figure 6: Temperature distribution in the vessel wall (wall thickness 40 mm) for different times.
Figure 7: Effective stress distribution in the vessel wall (wall thickness 130 mm) for different times

Figure 8: Effective stress distribution in the vessel wall (wall thickness 40 mm) for different times
Figure 9: Creep strain distribution in the vessel wall (wall thickness 130 mm) for different times

Figure 10: Creep strain distribution in the vessel wall (wall thickness 40 mm) for different times
Figure 11: Deformation of the vessel walls 75 min after start of the core melt scenario

Figure 12: Time history of the axial deformation in the center of the hot spot
STUDY OF RPV MATERIALS WITH RESPECT TO MECHANICAL BEHAVIOUR IN CASE OF COMPLETE CORE FUSION

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1. INTRODUCTION

This study deals with the mechanical behaviour of RPV low-alloy steel materials (present material Mn-Ni-Mo and a creep-resistant material Cr-Mo) in case of a severe accident with complete core fusion. The accident corresponds to a small primary break SBLOCA (pressure in the vessel = 2 MPa), with Safety Injection unavailable, but the RPV is externally cooled.

The thermal analysis is carried out using a simplified one-dimensional model with certain hypotheses concerning conduction in the steel, in the crust and in the molten pool, and thermal coefficients considered as a function of the boiling regime.

The thermo-mechanical axi-symmetrical analyses are made using the finite element SYSTUS code. Results are presented with respect to the creep damaged/undamaged thickness on the unfused part of the vessel resulting from the above accidental conditions.

One of the features of these analyses is that the transient is prolonged to a time period of 7 days and permits to see how the thermal shock gets reversed with time.

2. DESCRIPTION OF THE SCENARIO AND THE EROSION PHENOMENON

The hypothetical severe accident considered is a typical small primary break SBLOCA with Safety Injection unavailable on a PWR vessel with internal radius of the hemispherical part, R = 2687.5 mm and thickness of this part, t = 147.5 mm. The pressure remaining in the vessel is 2 MPa. Corium starts relocating at the bottom-head 3 h after the scram. The vessel is externally cooled with water at a temperature of 100°C; the external flooding is supposed to start 2 h after the scram.

At the contact between the liquid corium (2400°C) and the vessel lower head which is at a lower temperature (θ = 300°C), a crust of corium is formed, resulting in isolation of the liquid corium. The heat evacuated by the molten corium raises the temperature of the vessel steel under the crust. When the steel reaches its fusion temperature, the vessel thickness starts to melt under the crust. Due to the presence of molten steel, the crust becomes unstable and thus breaks, which leads once again to a direct contact of corium with steel and to the formation of a new crust. Thus the process of erosion of the vessel continues till a quasi-permanent state is reached when the total energy furnished by corium is evacuated through water. This phenomenon was identified during the SCARABEE (1) tests as being responsible for the erosion and in some cases the rupture of the component.

3. DESCRIPTION OF THE THERMAL ANALYSIS

3.1. Hypotheses

The following hypotheses used in the analysis are critically evaluated in §3.4.
*Thermal analysis is conducted using a simplified 1-D model where the flux distribution is supposed to be homogeneous on the whole surface of the vessel and the free surface of the adiabatic pool (i.e. no exchange with the exterior).

*Conduction in non-stationary regime is modelled using explicit finite-difference method.

*Since the crust formation is very slow, conduction in the crust is treated as successive stationary states.

*The layer of the liquid steel is treated in conduction without accumulation of heat.

*Thermo-mechanical properties of the materials (steel, corium) are considered constant during the transient and correspond to the mean value over the temperature range of the transient.

*Criterion used for the rupture of the crust is taken as follows:
    - minimum thickness of the corium crust = 3 mm
    and
    - maximum thickness of liquid steel layer = 3 mm

*Heat transfer towards water is considered as a function of the boiling regime.
*25% of the residual power is supposed to go out as volatile fission products while all the rest of the power is supposed to be evacuated through the hemispherical part, i.e. radiation at the free surface is considered to be negligible.

*Physical properties of both types of materials are considered the same. Thus one single thermal analysis is made for both the materials.

3.2. Initial Conditions

At time \( t = 3 \) h, the vessel is put in contact with corium at the internal surface. All the residual power is in the hemispherical part of the vessel, with a mean heat flux of about 0.8 MW/m². Water is already at the external surface. It is considered that a crust is formed instantaneously at the interface corium-vessel. The temperature at the interface is computed using the method of thermal-contact between two semi-infinite planes, and the crust thickness is evaluated using the equality of fluxes at the interface.

3.3. Results of the Thermal Analysis

*The hemispherical part of the vessel starts melting 490 s after the corium-vessel contact.

*The vessel erosion rate at this time, is 17 cm/h and decreases by stages to zero at 6450 s.

At this instant, the residual thickness of the vessel is 74 mm and the flux exchanged with water is maximum (\( = 593.6 \text{ kW/m}^2 \)).

*The initial corium crust thickness is 29 mm which reduces to 13 mm and remains at this value till the end of the erosion (\( t = 6450 \) s). From this time onward, the crust thickens to attain a value of 88 mm at the end of 7 days.

*Outside surface temperature of the vessel varies between 105.6°C and 116°C (end of erosion). It decreases then to attain a value of 111°C at the end of the transient. The boiling thus remains in the nucleate regime till the end of the transient.

Fig. 1 shows the temperature profile through the vessel wall thickness for three instants in the transient. It is observed that about 5 h after the scram (end of erosion), the temperature at the interface vessel-molten vessel, initially at 1400°C, decreases while temperature of corium remains at 2200°C (melting temperature of corium).

3.4. Critical Evaluation of the Thermal Analysis

The thermal analysis conducted here is a simplified 1-D analysis. It is known that in fact the convective movements in the molten pool induce heterogeneity of flux toward the vessel wall, which could result in a ratio of maximum flux to mean flux of 1.5 to 2, the hot spot being located slightly below the free surface depending on the % of energy evacuated through radiation from the free surface. However the assumption made here is that radiation flux from the pool is negligible.

Moreover, this analysis supposes that corium is homogeneous (i.e. metallic phase dispersed in the oxide phase). In reality, in the final state, the metallic phase is found above the oxides, and even though
the majority of the residual power is in the oxide phase, the hot spot could be in the metallic phase. Thus the thermal analysis should be considered as optimistic.

Finally, the thermo-mechanical properties of the materials (steel, corium) are considered constant during the transient and correspond to the mean value over the temperature range of the transient. This hypothesis is not fully accurate. However, the principal objective of this work was to evaluate the mechanical behaviour of the vessel materials; consequently, in the frame of this comparative thermo-mechanical study of the materials, where the the thermal transient and the thermal analysis in both types of materials is the same, these thermal analysis assumptions were considered sufficient at this stage.

4. THERMO-MECHANICAL ANALYSIS

4.1. Input Data

*Loading considered has two components:
  - evolution of the temperature through the vessel thickness since the beginning of the transient
  - internal pressure of 2 MPa considered constant throughout the transient

*As for material properties:
  - thermal expansion coefficient ($\alpha$, Table I), Young's modulus ($E$, Table II), and Poisson's ratio ($\nu$ = 0.3) are considered same for both the materials.

*For both the materials, the stress-strain law is that of a non-unified model i.e., total strain = elastic strain + plastic strain + creep strain, with isotropic hardening and secondary creep law. Both the materials are supposed to lose total strength from temperature, $\theta$ = 750°C onwards. Phase transformations are not taken into account explicitly in the computations, but rather through the mechanical properties which are function of temperature. Tables III and IV show the stress-strain behaviour for the two materials.

* Creep law used is that of Norton: \( \dot{\varepsilon} = K\sigma^n \)

Parameters $K_n$ have been identified using linear regression on $\log(\dot{\varepsilon})$ and $\log(\sigma)$, on a sufficiently large set of data obtained from several sources (2-6).

Tables V and VI present respectively the parameters for Mn-Ni-Mo and Cr-Mo materials.

4.2. Stress Computation

Stresses are computed using the SYSTUS code (2), one calculation for each of the materials. The evolution of the circumferential stress in case of the Mn-Ni-Mo material is shown on figs. 2 to 4:

* $t$ = 2h Only pressure loading, structure is in the elastic domain
* $t$ = 3h Pressure + differential temperature in the vessel wall, due to water flooding. Structure is still in the elastic domain
* $t$ = 3.04h Compression stress attains its maximum (427 MPa). Stress at the external surface goes on increasing.
* $t$ = 4h Stress at the external surface reaches its maximum (470 MPa). The compression zone goes on shifting although the compressive stress is still decreasing.
* $t$ = 5h Stress at the external surface decreases. Compressive zone stabilizes.
* $t$ = 8h Maximum tensile stress shifts toward the interior. A small tensile zone develops in the central portion. One can observe the start of reversal of the thermal shock.
* $t$ = 18h Level of tensile stress in the central portion becomes larger than that of external surface.
* $t$ = 24h Level of tensile stress in the central portion goes on increasing. External surface goes in compression.
* $t$ = 48h Compression stress at external surface becomes larger than that of internal part.
* $t$ = 103h Tension and compression stresses attain their maximum. Thermal shock is completely reversed.
* $t$ = 7 days End of the transient.

Within a few percentage, the radial stress is equal to the circumferential stress (spherical symmetry), while the other stress components are negligible.

For the Cr-Mo Material, the distribution of stresses is analogous to that of Mn-Ni-Mo material. Only the numerical values are different. Table VII compares the maximum and minimum values of the stresses in the two materials. One notices that after about 4h, the stresses in the Cr-Mo material become higher than those in the Mn-Ni-Mo material. This can be explained on the basis that the loading is essentially
«deformation controlled» type and that for plastic deformations greater than about 0.2%, the stress-strain curve of Cr-Mo material is higher than that of Mn-Ni-Mo material, for all temperatures.

As for the strains, their amplitude depends essentially on the maximum temperature attained during the transient. When this temperature is less than 750°C, the total strain (in absolute value) remains less than 0.5%.

5. DAMAGE ANALYSIS

The only damage mode considered is creep. If D is the damage variable,

D = 0 corresponds to the virgin material,

D = 1 corresponds to the completely damaged material

D is defined as

\[ D = \frac{\int_{t}^{T} dt}{T} \]

D = 0 for \( \theta \leq 370^\circ\text{C} \)

D = \int_{t}^{T} \frac{dt}{T} for \( 370 < \theta \leq 750^\circ\text{C} \)

D = 1 for \( \theta > 750^\circ\text{C} \)

with \( T = T_{\text{adm}}(\sigma(t), S_r(450^\circ\text{C})) \) for \( 370 < \theta \leq 450^\circ\text{C} \)

and \( T = T_{\text{adm}}(\sigma(t), S_r(\theta)) \) for \( 450 < \theta \leq 750^\circ\text{C} \)

where:

\( \sigma(t) \) is the von Mises equivalent stress at time t,

\( S_r \) is the rupture stress at the given temperature, and

\( T_{\text{adm}} \) is the rupture time for given temperature and stress values.

The damage criterion as defined above is applied on both the materials at every point of the structure. Fig.5 shows the cartography of the creep-damaged regions for both the materials. From this, one can notice that, although the stress level in the Cr-Mo material is higher than that in the Mn-Ni-Mo material, the damaged zone is more restricted. This can be explained on the basis of the differences in the creep-rupture stress levels.

Moreover, for the Cr-Mo material, the limit between undamaged (D<0.1) and damaged material (D=1) corresponds to the attainment of temperature, \( \theta > 750^\circ\text{C} \). This shows that the triplet (stress \( \sigma \), hold time at stress \( \sigma \), temperature \( \theta \)) is not sufficient to lead the structure to damage for \( \theta < 750^\circ\text{C} \). However, for the Mn-Ni-Mo material, the limit between damaged and undamaged parts is well defined by creep properties. For comparison sake, the thickness for which D<0.1 is given by

\[ t = 18.4 \text{ mm for Mn-Ni-Mo material} \]
\[ t = 32.0 \text{ mm for Cr-Mo material} \]

on an unfused thickness of about 74 mm.

6. CONCLUSION

The present paper shows the mechanical behaviour of two types of vessel materials during a severe accident transient. It is observed that, for the case of the chosen hypothesis of the accidental conditions, an undamaged thickness can remain. In such a case the Cr-Mo steels show more resistance than the Mn-Ni-Mo steels, because of their better creep properties.

7. REFERENCES

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(3) G. V. SMITH


(4) G. V. SMITH


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(6) J. R. WOLF and J. L. REMPE
OECD NEA-TMI 2 Vessel Investigation Project,

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<table>
<thead>
<tr>
<th>θ (°C)</th>
<th>0</th>
<th>150</th>
<th>300</th>
<th>450</th>
<th>600</th>
<th>750</th>
</tr>
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<tbody>
<tr>
<td>E (GPa)</td>
<td>193</td>
<td>190</td>
<td>181</td>
<td>169</td>
<td>136</td>
<td>69.5</td>
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</table>

<table>
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<tr>
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<th>1.15</th>
<th>1.29</th>
<th>1.37</th>
<th>1.41</th>
<th>1.41</th>
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<tr>
<td>θ (°C)</td>
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<td>1050</td>
<td>1200</td>
<td>1350</td>
<td>1500</td>
<td></td>
</tr>
<tr>
<td>E (GPa)</td>
<td>24</td>
<td>7</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td></td>
</tr>
</tbody>
</table>

**Table I** Evolution of Thermal expansion coefficient as a function of Temperature

**TABLE II** Evolution of Young's Modulus as a function of Temperature
### Table III Stress (MPa) corresponding to a given plastic deformation at a given temperature (MN-Ni-Mo steel)

<table>
<thead>
<tr>
<th>θ (C)</th>
<th>0.000</th>
<th>0.010</th>
<th>0.020</th>
<th>0.030</th>
<th>0.040</th>
<th>0.050</th>
<th>0.060</th>
<th>0.070</th>
<th>0.080</th>
<th>0.090</th>
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<tr>
<td>20</td>
<td>485.0</td>
<td>490.1</td>
<td>537.0</td>
<td>575.0</td>
<td>602.5</td>
<td>625.0</td>
<td>643.0</td>
<td>661.1</td>
<td>677.2</td>
<td>691.4</td>
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<td>200</td>
<td>425.0</td>
<td>445.1</td>
<td>487.1</td>
<td>515.9</td>
<td>539.0</td>
<td>560.0</td>
<td>570.4</td>
<td>580.8</td>
<td>589.8</td>
<td>597.4</td>
</tr>
<tr>
<td>300</td>
<td>405.0</td>
<td>432.8</td>
<td>480.0</td>
<td>511.9</td>
<td>534.0</td>
<td>549.9</td>
<td>566.3</td>
<td>582.8</td>
<td>596.8</td>
<td>608.6</td>
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<tr>
<td>400</td>
<td>385.0</td>
<td>424.8</td>
<td>459.8</td>
<td>477.9</td>
<td>490.0</td>
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<td>500</td>
<td>345.0</td>
<td>383.1</td>
<td>400.0</td>
<td>407.0</td>
<td>410.0</td>
<td>412.5</td>
<td>415.0</td>
<td>417.5</td>
<td>420.0</td>
<td>422.5</td>
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<tr>
<td>600</td>
<td>270.0</td>
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<td>283.5</td>
<td>287.0</td>
<td>287.0</td>
<td>287.0</td>
<td>287.0</td>
<td>287.0</td>
<td>287.0</td>
<td>287.0</td>
</tr>
<tr>
<td>650</td>
<td>205.0</td>
<td>205.5</td>
<td>206.0</td>
<td>206.5</td>
<td>207.0</td>
<td>207.6</td>
<td>208.1</td>
<td>208.6</td>
<td>209.1</td>
<td>209.6</td>
</tr>
<tr>
<td>700</td>
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<td>140.5</td>
<td>141.0</td>
<td>141.5</td>
<td>142.0</td>
<td>142.5</td>
<td>143.1</td>
<td>143.6</td>
<td>144.1</td>
<td>144.6</td>
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<tr>
<td>750*</td>
<td>10.0</td>
<td>10.0</td>
<td>10.0</td>
<td>10.0</td>
<td>10.0</td>
<td>10.0</td>
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<td>10.0</td>
<td>10.0</td>
<td>10.0</td>
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### Table IV Stress (MPa) corresponding to a given plastic deformation at a given temperature (Cr-Mo steel)

<table>
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<tr>
<th>θ (C)</th>
<th>0.0005</th>
<th>0.001</th>
<th>0.0051</th>
<th>0.006</th>
<th>0.007</th>
<th>0.008</th>
<th>0.015</th>
<th>0.02</th>
<th>0.03</th>
<th>0.04699</th>
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<td>20</td>
<td>429.0</td>
<td>452.0</td>
<td>475.0</td>
<td>500.7</td>
<td>504.0</td>
<td>508.2</td>
<td>513.0</td>
<td>539.0</td>
<td>557.0</td>
<td>591.0</td>
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<tr>
<td>350</td>
<td>356.0</td>
<td>377.0</td>
<td>398.0</td>
<td>440.3</td>
<td>446.0</td>
<td>450.2</td>
<td>456.0</td>
<td>481.0</td>
<td>494.0</td>
<td>516.0</td>
</tr>
<tr>
<td>482</td>
<td>349.0</td>
<td>358.7</td>
<td>368.2</td>
<td>411.0</td>
<td>418.0</td>
<td>424.0</td>
<td>430.3</td>
<td>461.8</td>
<td>479.8</td>
<td>502.2</td>
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<tr>
<td>565</td>
<td>301.0</td>
<td>311.9</td>
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<td>366.4</td>
<td>368.7</td>
<td>371.7</td>
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<td>394.4</td>
<td>400.6</td>
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<tr>
<td>650</td>
<td>189.0</td>
<td>207.0</td>
<td>225.7</td>
<td>284.1</td>
<td>285.5</td>
<td>286.5</td>
<td>287.0</td>
<td>287.0</td>
<td>287.0</td>
<td>287.0</td>
</tr>
<tr>
<td>750*</td>
<td>20.0</td>
<td>20.0</td>
<td>20.0</td>
<td>20.0</td>
<td>20.0</td>
<td>20.0</td>
<td>20.0</td>
<td>20.0</td>
<td>20.0</td>
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*Nota: The values at 750°C do not correspond to a physical reality. These are given only to show that the mechanical resistance at this temperature is negligible.
Table V Parameters of the NORTON law for Mn-Ni-Mo materials

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>K (1/h MPa(^n))</th>
<th>n</th>
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<tr>
<td>454</td>
<td>3.11 \times 10^{-33}</td>
<td>11.43</td>
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<td>510</td>
<td>3.34 \times 10^{-31}</td>
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<td>627</td>
<td>2.65 \times 10^{-13}</td>
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<td>652</td>
<td>1.36 \times 10^{-12}</td>
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<td>677</td>
<td>2.49 \times 10^{-12}</td>
<td>5.02</td>
</tr>
<tr>
<td>727</td>
<td>3.38 \times 10^{-11}</td>
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</tr>
<tr>
<td>750</td>
<td>1 \times 10^{-4}</td>
<td>5.08</td>
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Table VI Parameters of the NORTON law for Cr-Mo materials

<table>
<thead>
<tr>
<th>Time (h)</th>
<th>(\sigma_{zzx}^*) MPa</th>
<th>(\sigma_{zzx}^\text{min}) MPa</th>
<th>(\sigma_{zzm}^*) MPa</th>
<th>(\sigma_{zzm}^\text{min}) MPa</th>
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<tr>
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<td>17.1</td>
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<td>-332.8</td>
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<td>8h</td>
<td>326.8</td>
<td>363.4</td>
<td>-241.1</td>
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<td>18h</td>
<td>190.3</td>
<td>205.7</td>
<td>-186.2</td>
<td>-256.9</td>
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<tr>
<td>24h</td>
<td>237.0</td>
<td>259.0</td>
<td>-176.3</td>
<td>-252.2</td>
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<tr>
<td>48h</td>
<td>304.0</td>
<td>330.5</td>
<td>-223.1</td>
<td>-276.6</td>
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<tr>
<td>103h</td>
<td>357.7</td>
<td>383.6**</td>
<td>-499.7</td>
<td>-526.8</td>
</tr>
<tr>
<td>7 days</td>
<td>340.7</td>
<td>384.4</td>
<td>-475.6</td>
<td>-505.9</td>
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</table>

\(\sigma_{zzx}^*\) : Maximum value ; \(\sigma_{zzm}^*\) : Minimum value

**For Cr-Mo steel, max. occurs at 126h (390 MPa)

Table VII Comparison between Mn-Ni-Mo and Cr-Mo materials with respect to Circumferential stress

Fig. 1 Temperature profile through the vessel wall thickness

Fig. 2 Evolution of Circumferential Stress as a function of time (2h to 4h) for Mn-Ni-Mo steel
Fig. 3 Evolution of Circumferential Stress as a function of time (5h to 24h) for Mn-Ni-Mo steel

Fig. 4 Evolution of Circumferential Stress as a function of time (48h to 175h) for Mn-Ni-Mo steel

Fig. 5 Cartography of creep-damaged regions (Cr-Mo and Mn-Ni-Mo steels)
NATURAL CONVECTION BOILING ON THE OUTER SURFACE
OF A HEMISPHERICAL VESSEL SURROUNDED BY
A THERMAL INSULATION STRUCTURE

F.B. Cheung and Y.C. Liu
Department of Mechanical Engineering
The Pennsylvania State University
University Park, PA 16802

ABSTRACT
The phenomena of steam venting and critical heat flux for natural
convection boiling on the outer surface of a heated hemispherical vessel
surrounded by a thermal insulation structure were investigated
experimentally. The objectives were to observe the behavior of the
boiling-induced two-phase motion in the annular gap and to determine the
flow effect on the critical heat flux. High-speed photographic records
revealed the presence of violent cyclic ejection of the vapor masses
generated by boiling on the vessel outer surface which resulted in a
buoyancy-driven, upward, co-current two-phase flow through the channel.
When boiling was taking place at high heat flux levels, the flow through
the minimum gap was found to be highly unsteady and chaotic. Measurements
of the local boiling heat fluxes and the local wall superheats were made
under steady-state boiling conditions covering the entire range of nucleate
boiling up to the local critical heat flux.

INTRODUCTION
The method of external passive cooling of the reactor pressure vessel
(RPV) by flooding the reactor cavity has been considered as a viable means
of decay heat removal during a severe accident. Design features of most
advanced light water reactors (ALWRS) have the provision for substantial
water accumulation within the containment during numerous postulated
accident sequences. With the water covering the lower external surfaces of
the RPV, should the accident progress to the slumping of core debris into
the lower vessel head, significant energy (i.e., decay heat) could be
removed from the core melt through the vessel wall by nucleate boiling on
the vessel outer surface. As long as the wall heat flux from the core melt
would not exceed the critical heat flux (CHF) limit for nucleate boiling on
the vessel outer surface, the reactor vessel could be sufficiently cooled
so as to prevent downward failure of the vessel and release of the core
melt into the containment.

In many ALWR designs, the reactor vessel is surrounded by a thermal
insulation structure as shown schematically in Figure 1. The insulation
structure, having a cylindrical upper part and an octagonal lower part,
forms a hemispherical annular gap with the reactor vessel. As boiling of
the water takes place on the vessel outer surface under severe accident
conditions, the vapor masses generated on the heating surface tend to flow
upward through the channel under the influence of gravity. Because of the
vapor motion, liquid water is entrained in the flow, thus resulting in an
upward co-current two-phase motion in the channel. While the flow is
induced entirely by the boiling process, the rate of boiling, in turn, can
be significantly affected by the resulting two-phase motion. Thus far,
this unconventional natural convection boiling process has not been
investigated by previous researchers.
The major objectives of the present work are to observe the vapor dynamic and the steam venting process in the hemispherical channel depicted in Figure 1 and to determine the critical heat flux for natural convection boiling on the downward facing curved heating surface.

**EXPERIMENTAL SETUP**

The experimental apparatus consisted of a water tank with a condenser unit, a test vessel with a scaled thermal insulation structure, a data acquisition and photographic system, and a power control system. The water tank was made of carbon steel and had dimensions shown in Figure 2. Three immersion heaters were installed near the bottom of the tank to preheat the water to a desired temperature before a run. The water level was maintained constant in the tank during a run by using a condenser unit on top of the water tank to condense the vapor masses generated by boiling with the condensate being returned to the bottom of the tank.

The test vessel, made of pure aluminum, had a diameter of 0.3m. It consisted of a heated hemispherical lower part to simulate the lower head of a reactor vessel, and an unheated cylindrical upper part to simulate the upper part of a reactor vessel. The hemispherical lower part of the test vessel was 0.15m in height whereas the cylindrical upper part was 0.7m in height. There were five segments in the lower part, each having the same heat transfer area. Uniformly spaced independent heating elements were installed on the interior side of each segment to provide a local wall heat flux up to 1.2 MW/m². The test vessel was surrounded by a scaled thermal insulation structure (see Figure 3) that was fabricated to simulate the hydrodynamic aspects of the thermal insulation system of an advanced light water reactor. The scaled insulation structure, having a nominal diameter of 0.46m, consisted of an upper cylindrical part and a lower octagonal part. The upper part was approximately 0.72m in height and had six horizontal opening slots (each having a length of 0.08m) near the top for steam venting. The lower part was made up of eight equal-sized panels with a small opening (approximately 0.038m in diameter) at the bottom center for water ingestion, as shown in Figure 3. Both parts were made of plexiglas material so that the entire structure was transparent, suitable for direct flow observations.

The scaled insulation structure was mounted symmetrically to the aluminum test vessel to form a flow annulus. To do this, the upper part of the insulation structure was properly aligned with the center cover plate of the water tank to assure a uniform circumferential gap between the test vessel and the insulation structure. By changing the vertical position of the center cover plate using annular spacers, the height of the insulation structure submerged in the water could be adjusted. In so doing, the size of the minimum gap between the insulation structure and the hemispherical lower part of the test vessel could be varied.

K-type thermocouples were embedded at various locations inside the hemispherical lower part of the test vessel for temperature measurements. The thermocouple signals were recorded by using an IBM compatible personal computer along with a data acquisition system. Two Strawberry Tree ACPC-16 boards were installed inside the PC. Each board had 16 analogy inputs and 16 digital input/output channels. The system was capable of monitoring up to 32 thermocouple signals. The ACPC-16 board was capable of resolutions in the range between 12 and 16 bits, which was equivalent to 0.024% and 0.0015% of full scale, respectively. Each of the boards had six voltages ranges that could be set according to the sensor used. The boards also had a high noise rejection integration converter, which helped reject 50/60 Hz AC power line interference when used in the "low noise mode". The ACPC-16 units were also capable of accurate cold junction compensation and linearization for thermocouple devices. The two ACPC-16 boards were
connected to a total of four Strawberry Tree T12 boards, which in turn were connected to the thermocouples. Each of the T12 boards had 8 analog inputs and 8 digital input/output channels. The recorded temperatures were then analyzed using an in-house inverse heat conduction code (Liu 1995) to determine the local boiling heat fluxes and the local wall superheats. For heat flux levels above 0.1 MW/m², which is the range anticipated under severe accident conditions, the relative error in the heat flux measurement was estimated to be ±7%.

The photographic system consisted of a Minolta X-370 high speed camera and a Kodak Ektapro high speed video system. The high speed video system consisted of a motion analyzer, an imager, a cassette conditioner and a TV set. The motion analyzer was used to set the speed of videotaping to a value as high as 1000 frames/sec. Once a session was recorded, the motion analyzer was used to play the recorded boiling phenomenon on the TV screen. In addition, it was possible to play the event one frame at a time, which was very helpful in studying the characteristics of the vapor dynamics on the vessel outer surface.

When the critical heat flux was reached during steady state heating experiments, any further increase in the power input could result in the onset of film boiling. This was characterized by a sudden large increase in the temperature of the heating surface. In order to protect the vessel against any possible meltdown, a control mechanism was installed to discontinue the power supply to the heaters when a significant jump in the vessel temperature was detected in the high heat flux regime. The control system consisted of a data acquisition system, a constant DC power source, a solid state relay, and thermocouples to measure the vessel wall temperature. The solid state relay had a low voltage side connected to the constant DC power source, and a high voltage side connected to the variac supplying the heaters.

VAPOR DYNAMICS AND STEAM VENTING PHENOMENON

Photographic studies were performed to seek a clear physical understanding of the vapor dynamics and two-phase flow through the annular gap between the test vessel and the scaled insulation structure under steady-state boiling conditions. The video records showed that at high heat flux levels, large elongated vapor masses or slugs, being squeezed against the wall by the local buoyancy force, grew periodically on the vessel outer surface. They were then ejected violently upward in all directions, resulting in an upward co-current two-phase flow through the annular gap. The two-phase motion appeared to be in the churn turbulent regime. Close observation of the flow clearly revealed that it was three-dimensional with swirl formation when boiling was taking place at high heat flux levels. Strong upstream influences were observed as a result of the activities of large elongated bubbles in the bottom center region of the vessel.

Over the range of heat flux levels (0.1 - 1.2 MW/m²) explored in the experiments, violent two-phase motions were observed in the annular gap. The rate of steam venting appeared to increase considerably with the power input. Close observation of the two-phase flow phenomenon in the minimum gap region was made at high heat flux levels. Large vapor slugs, occupying nearly the entire cross-sectional flow area, were observed to flow through the minimum gap in a highly unsteady and chaotic manner, especially at heat flux levels close to the local CHF limit. This unsteady, chaotic feature was probably caused by the high-frequency cyclic vapor ejection process associated with the downward facing boiling on the external bottom surface of the test vessel, and the three-dimensional swirls generated by the strong recirculation motions in the annular gap.
NATURAL CONVECTION BOILING HEAT TRANSFER

The steady-state nucleate boiling data for the high-heat-flux regime measured at various locations of the test vessel are shown in Figures 4-8. In these figures, the local nucleate boiling heat fluxes are plotted against the local wall superheats with the water temperature as a parameter, which was varied in the experiments from 90°C for the case of subcooled boiling to 100°C for the case of saturated boiling. In contrast to the conventional case of pool boiling for which liquid subcooling has very little effect on nucleate boiling heat transfer, in the present case, a strong subcooling effect was observed at various locations of the vessel outer surface. The nucleate boiling curve in the entire high-heat-flux region shifted upward as the degree of subcooling was increased. This enhancement in the subcooled nucleate boiling heat transfer was evidently due to the effect of the upward co-current two-phase flow in the annular channel.

Because of the buoyancy-driven co-current two-phase flow that was induced by the boiling process, there was a significant variation of the nucleate boiling heat flux along the vessel outer surface. This was the case for both saturated and subcooled boiling, as shown in Figures 4 to 8. For a given water temperature, the nucleate boiling heat flux tended to increase in the downstream direction, evidently due to the natural convection flow effect. Although it was a pool boiling process, the phenomenon exhibited strong flow boiling characteristics. Note however that the nucleate boiling rate did not increase monotonically in the downstream location. Rather it underwent an adverse change in the minimum gap region, as can be seen from Figures 5 and 7.

The present results were compared to those corresponding data reported by Cheung, Haddad and Liu (1997) for the case of “external” natural convection boiling on the outer surface of a hemispherical vessel without an insulation structure. It was found that under both saturated and subcooled boiling conditions, the nucleate boiling heat fluxes for the case with thermal insulation were consistently higher than the corresponding values for the case without thermal insulation. This difference in the nucleate boiling heat transfer was attributed to the buoyancy-driven two-phase flow effect. For the case without thermal insulation (Cheung, Haddad and Liu 1997), an “external” natural convection two-phase boundary layer flow was induced by the boiling process. The mass flow rate increased in the downstream locations from its minimum value at the bottom center to its maximum value at the equator of the hemispherical vessel. Hence the flow effect was relatively weak in the bottom center region. On the other hand, an “internal” natural convection two-phase flow was induced by the boiling process in the present case with an insulation structure. The mass flow rate was essentially constant throughout the entire flow channel. Hence the flow effect was relatively strong in the bottom center region compared to the corresponding case without thermal insulation. This resulted in a higher heat transfer rate.

SPATIAL VARIATION OF THE CRITICAL HEAT FLUX

For the case without thermal insulation, Cheung and Haddad (1997) found that the local critical heat flux is a monotonically increasing function of the angular position on the vessel outer surface. It had a minimum value at the external bottom center of the vessel and a maximum value at the equator (i.e., upper edge) of the vessel. A radically different spatial variation of the CHF limit was observed in this study with thermal insulation. Local dryout of the wall was not detected at the bottom center of the vessel even for saturated boiling at the heat flux level of 1.2
MW/m². As the heat flux was increased to 1.2 MW/m², the local wall temperature at the bottom center of the test vessel simply rose to a new steady-state value without showing any abrupt increase in magnitude. This clearly indicated that, with the presence of an insulation structure, the local CHF limit for saturated boiling on the external bottom center of the vessel was larger than 1.2 MW/m². This was almost three times higher than the local CHF value of 0.45 MW/m³ for the case without thermal insulation (Cheung, et al. 1997).

Near the minimum flow gap (θ ~ 45°), the local CHF limit for saturated boiling was found to be approximately 0.98 MW/m². This was considerably lower than the local CHF limit at the external bottom center. Physically, this decrease in the local critical heat flux was evidently due to the difficulty in venting the steam through the minimum flow gap. As the local CHF limit was approached, the vapor slug tended to occupy the entire cross-sectional flow area at the minimum gap, thus preventing the supply of fresh liquid water to the heating surface. This resulted in a premature dryout of the surface, leading to a considerably smaller value of the local critical heat flux. In view of this, the minimum gap region could be a potential hot spot on the vessel wall under severe accident conditions.

CONCLUSIONS

Based upon the flow observation and the heat transfer data obtained in this study, the following conclusions can be made:
1. As a result of natural convection boiling on the outer surface of the hemispherical test vessel, an upward co-current two-phase flow, driven entirely by buoyancy, was induced in the annular gap between the test vessel and the insulation structure.
2. There were strong upstream influences on the flow field due to the activities of large elongated vapor masses generated by boiling in the bottom center region of the vessel.
3. The rate of steam venting appeared to increase considerably with the power input. Large vapor slugs, occupying the entire cross-sectional flow area, were observed to flow through the minimum gap in a highly unsteady and chaotic manner.
4. Because of the chaotic steam venting process through the minimum gap, the local critical heat flux near the minimum gap was considerably lower than the CHF limit at the bottom center. The minimum gap region could be a potential hot spot on the vessel wall under severe accident conditions.

ACKNOWLEDGMENT

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REFERENCES


Figure 1. Schematic of the Reactor Vessel in a Flooded Cavity Surrounded by a Thermal Insulation Structure.

Figure 2. Dimensions of the Water Tank with a Condenser Unit Employed in the Experiment.

Figure 3. Schematic of the Simulated Reactor Vessel Surrounded by a Scaled Insulation Structure in the Water Tank.

Figure 4. Natural Convection Boiling Data at the External Bottom Center of the Vessel ($\theta = 0^\circ$).
Figure 5. Natural Convection Boiling Data at an External Off-Center Location of the Vessel ($\theta = 18^\circ$).

Figure 6. Natural Convection Boiling Data at an External Off-Center Location of the Vessel ($\theta = 45^\circ$).

Figure 7. Natural Convection Boiling Data at an External Off-Center Location of the Vessel ($\theta = 60^\circ$).

Figure 8. Natural Convection Boiling Data at an External Off-Center Location of the Vessel ($\theta = 75^\circ$).
INTRODUCTION

In case of severe accident, a molten pool may form at the bottom of the lower head, and some pessimistic scenarios estimate that heat fluxes up to 1.5 MW/m$^2$ should be transferred through the vessel wall. An efficient, though completely passive, removal of heat flux during a long time is necessary to prevent total wall ablation, and a possible solution is to flood the cavity with water and establish boiling in natural convection. High heat exchanges are expected, especially if the system design (deflector along the vessel, riser ...) emphasize water natural circulation, but are unfortunately limited by the critical heat flux phenomena (CHF).

CHF Data are very scarce in the adequate range of hydraulic and geometric parameters and are clearly dependent of the system effect in natural convection. The system effect can both modify flow velocity and two phase flow regimes, counter-current phenomena and flow static or dynamic instabilities.

SULTAN purpose was of two kinds, increasing CHF Data for realistic situations, and improving the modeling of large 3D two phase flow circuits in natural convection.

The CATHARE thermal-hydraulic code is used for interpreting the data and for extrapolation to real geometry. As a first step, a one-dimensional model is used. It is shown that some closure laws have to be improved. Reasonable predictions may be obtained but, for some test conditions, multi-dimensional effects such as recirculation appear to be dominant. Therefore the 3-dimensional module of CATHARE is also used to investigate these effects. This model well predicts qualitatively the existence and the development of a 2-phase layer along the heated wall as well as the existence of a recirculation zone. But modelling problems still require further development as part of a long term program for a better prediction of multi-dimensional two-phase flows.

SULTAN TEST FACILITY

SULTAN Program is supported by CEA, EdF and FRAMATOME. The SULTAN facility (figure 1) was designed as a full scale analytical forced convection experiment, on a wide range of parameters covering most of the situations involved in a slow transitory situation after a severe accident (mass velocity : 10 => 5000 kg/s/m$^2$, pressure : 0.1 => .5 MPa, inlet subcooling : 50 => 0 °C, heat flux : 0.1 => 1 MW/m$^2$ (with some data up to 2 MW/m$^2$)). Fluid is demineralized and degassed water.
The Test section itself (figure 2) is simplified regarding reality, the purpose being to validate
codes and calculate as many different realistic situations as desired. It is a flat plate, 1.5 mm
thick, 4 m long and 15 cm wide, uniformly electrically heated, in a rectangular channel.
Channel width (gap) can be enlarged from 3 to 15 cm, and the test section can be inclined
from vertical to horizontal position. It is highly instrumented: mass velocity, electric power,
absolute and differential pressures, wall temperatures, fluid temperatures, local void fraction,
large windows for video films and high speed films. Precision of measurement is between 1
and 3 % of the measures.

TEST PROCEDURES

Eight campaigns of tests have been performed, each campaign involving one inclination and
one gap of the test section, with the following range of parameters:

<table>
<thead>
<tr>
<th>Campaign No.</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
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<tbody>
<tr>
<td>Inclination (°)</td>
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<td>10°</td>
<td>90°</td>
<td>45°</td>
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<td>90°</td>
</tr>
<tr>
<td>Gap (cm)</td>
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<td>15</td>
<td>15</td>
<td>15</td>
<td>6</td>
<td>6</td>
<td>15</td>
</tr>
<tr>
<td>Heated length (m)</td>
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<td>4</td>
<td>4</td>
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<td>4</td>
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</tr>
<tr>
<td>Cover pressure (MPa)</td>
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<td>0.1-0.5</td>
<td>0.1-0.5</td>
<td>0.1-0.5</td>
<td>0.1-0.5</td>
<td>0.1-0.5</td>
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<tr>
<td>Inlet subcooling (°C)</td>
<td>0-50</td>
<td>0-50</td>
<td>0-50</td>
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</tr>
<tr>
<td>Heat fluxes (kW/m²)</td>
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<td>100 to 1000 step 100</td>
<td>100 to 1000 step 100</td>
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<tr>
<td>Mass velocity (kg/s/m²)</td>
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<td>10 to 2000</td>
<td>10 to 2000</td>
<td>10 to 2000</td>
<td>20 to 5000</td>
<td>20 to 5000</td>
<td>10 to 4000</td>
<td>10 to 2000</td>
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Two kind of tests were performed:

Pressure drops and CHF limits tests: for a constant cover pressure, inlet subcooling and
uniform heat flux, mass velocity is slowly reduced with a relative ratio of 2% per minute,
pressure drops and fluid temperatures are measured every 10 s. Limit of boiling crisis may
be obtained before minimal flow rate, and is determined by a sharp increase of one or more
of the thermocouples welded on the heated plate above a level set at Saturation temperature
+ 150°C.

Spacial local characteristics of two phase flow: for constant cover pressure, inlet
subcooling, heat flux and mass velocity, void fraction and water temperature are measured
at 25 positions in the test section gap and for 4 elevations along the test section. Temporal
convergence of measures is assured with 30 to 600 s of integration for one position.
EXPERIMENTAL RESULTS

Two phase flow in SULTAN channel was thoroughly observed and measured, in order to better predict and calculate the behavior of a complete natural convecting system, with an emphasis on the evaluation of the recirculating mass flow rate and the static stability of the system based on the Internal and External Characteristics method [1],[2].

Experimental results: flow behavior

A general description of the flow in the test section will first try to sum up all the information provided by local measurements and films (figure 3).

First, a thermal layer develops near the heated plate, but never reaches the opposite side in large gaps or inclined positions of the test section. Thermal stratification is observed for inclined positions. Due to the low inlet flow velocities, mixed convection regimes are common in the test section, inducing internal recirculation cells which tend to homogenize temperatures, profiles of temperature become flatter, a secondary maximum can be observed on the cold wall opposite to the heated wall.

A two phase layer starts to develop in subcooled conditions. Subcooling is dependent of heat flux, flow velocity and test section inclination and can be up to 50 °C. Bubbles are first separate, with a oblong shape, about 3 or 4 cm long and 1 cm thick, which coalesce when they become numerous.

Generation of vapor is poorly predicted by correlations like Saha-Zuber's [3], it is probably partly due to the fact that this correlation was established for smaller and uniformly heated channels A new correlation will be optimized but is not yet available.

The two phase layer thickens in a more or less wavy manner, its development is highly non linear and increases much faster when saturation is imminent. In subcooled conditions, it never invades the whole channel and the maximum void fraction, up to 40% is always located on the heated plate.

When saturation is reached , the vapor invades the whole channel, even for large gaps and low inclinations. Stratification is important: for inclined positions of test section, the maximum of void fraction remains on or very near the heated plate whereas, for vertical positions, it moves toward the center of the channel, and can reach 90%.

Two different regimes may be observed: the first one corresponds to a rather steady two phase flow, with the particularity of heterogeneous vapor inclusions in size, from a few mm to about 1 m.

The second regime is pulsated flow with a period of 1 to 3 seconds, big pockets of vapor develop and are washed away periodically, count current water follows. CHF is avoided by a persisting thin film of liquid on the heated plate. Such a regime was described by Theofanous [4] and Chu [5] on the bottom of their hemispherical test sections. It was observed on SULTAN facility for inclinations of 10°, up to 1MW/m² for gap 3 cm, but only up to 500 kW/m² for gap 15 cm. Though its erratic aspect, this regime does not modify the average pressure drops in the test section, nor the limits of CHF.

A phenomena of reversal flow, with water flowing back from the pipe between the end of the test section and the condenser, was observed when outlet mean velocity was low enough, that is to say for any configurations at low heat fluxes, but only for vertical position, gap 15 cm and pressure 0.5 MPa at 1 MW/m². This phenomena improves considerably the limit of CHF, but is not easily predicted by the existing correlations of flow reversal, like Wallis' or Puskina and Solokin [6].

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Experimental results: Pressure drops

The Internal Characteristics (IC) of the test section, i.e. the variation of pressure drop versus the mass velocity for constant thermohydraulic conditions of pressure, heat flux and inlet subcooling, were systematically investigated.

For vertical position (figure 4), the IC shape is the same for any gap, pressure, inlet subcooling and heat flux: the slope of the IC is quite flat at high mass flow velocity and tend to gravity head value, then becomes steeper after average saturation is reached. CHF always occurs rather low on the steep slope, for saturation conditions.

For that kind of IC curves, natural circulation should be efficient up to 1 MW/m² and even more, provided that the rest of the circuit is designed for little friction pressure drops. A two phase adiabatic riser above the heated length could improve significantly the performance of the circuit. No static instabilities are expected as the IC curve is strictly monotonous. Dynamic instabilities should be of small amplitude thanks to the steepies of the slope.

For inclined position of 10°, the behavior is more complex and of three kind: at low heat fluxes (< 400 kW/m²), there is no difference with the vertical position. For large gap of 15 cm and high heat fluxes, CHF occur on the flat part of the IC, before average saturation is reached, there is no opportunity that steady natural convection should be established. For small gaps (figure 5) of 3 and 6 cm and high heat fluxes, boiling start at high mass velocity and the IC curves tend to the 'S' shape measured in small channels. Static instability is there expected in natural convection, with a rapid reduction of flow rate and destruction of the heated plate.

Experimental results: CHF Limits

191 CHF Data have been obtained on SULTAN facility. Dry patches are generally rather small (2-6 cm²) and cannot expend much due to the thinness of the heated plate. Though CHF location is expected at the end of a uniformly heated test section, many dry patches have occurred at lower elevation, within the last meter and even the last two meters for inclined positions. In term of local quality, it still represent little difference, but was taken into account in the SULTAN CHF Correlation giving Heat Flux (F) in MW/m², in term of Cover Pressure (P) in MPa, mass velocity (G) in kg/s/m², local thermodynamic quality (X), gap (E) in m and inclination (θ = sin (I), I inclination above horizontal):

\[ F = A0(E,G) + A1(E,G)X + A2(E)X^2 + A3(E,P,G,X)\theta + A4(E,P,G,X)\theta^2 \]

Standard deviation: 9.7%

with \( G' = LN(G) \)

\[ A0 = b0 + b1^*E^*G' + b2/P^2 + b3^*G + b4^*E/P + b5^*E/P^2 + b6^*E^*G'^2 \]
\[ A1 = b7^*G'2 + b8^*E^*G' \]
\[ A2 = b9^*E \]
\[ A3 = b10^*G'^2 + b11^*E^*P + b12^*X^*G' \]
\[ A4 = b13^*P + b14^*G' + b15^*X + b16^*E \]

<table>
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<td>-1.8898</td>
<td></td>
</tr>
</tbody>
</table>

354
The term \( A_0 + A_1 \cdot X \) represents the general behavior of CHF phenomena, extensively studied for PWR conditions [7], [8], [9]: heat flux decreases almost linearly with \( X \), with a positive influence of \( G \) at low quality and a negative one at high quality.

The term \( A_2 \cdot X^2 \) expresses the fact that, for high quality, the curves \( F \) versus \( X \) tend toward an asymptotic flat line: due to flooding phenomena, the heated plate is wetted by counter current water and boiling crisis is suppressed. In that particular case, boiling crisis does not depend any more of the conditions at the outlet of the test section, but of the amount of water stored above it and of the delay before uncovering. Flooding phenomena is correlated to low outlet velocities, it was then more or less observed on SULTAN for all campaigns at low heat fluxes, but only restricted to vertical position, gap 15 cm and pressure 0.5 MPa for heat fluxes up to 1 MW/m\(^2\) (campaign 3) and even 2 MW/m\(^2\) (campaign 8). This configuration was then maintained for more than 2 hours, until uncovering.

The influence of inclination is expressed through the terms \( A_3 \cdot \Theta + A_4 \cdot \Theta^2 \). As expected [10], [11], heat flux decreases when inclination increases. The expression is very similar to the correlation obtained on the ULPU experiment, though it is difficult to compare as ULPU correlation does not take into account the effect of gap, pressure, velocity and local subcooling or quality, this last parameter being predominant.

The other parameters have a limited influence: \( F \) increases slightly when the pressure \( P \) increases. Gap seems to have no effect when CHF is reached for saturated conditions (generally in vertical position) and a limited positive effect in subcooled conditions (corresponding to inclined positions of test section).

**OBJECTIVES OF THE CATHARE STUDY - 2 APPROACHES**

The CATHARE thermalhydraulic code is used for interpreting the data and for extrapolating to the real geometry. Two approaches are proposed:

- **1-D approach** based on the 1-D modelling of the test section. The objective is then to handle a tool as simple and as fast as possible, assessed against the SULTAN experimental data. The objective is therefore to be able to recalculate the SULTAN experiment in order to predict, on one hand, the \( \Delta P \)'s which control the natural circulation, and on the other hand, the heat transfer coefficient and the CHF limits. It will allow to extrapolate the SULTAN experimental results to predict the system efficiency in real geometry (figure 6).

Anyhow this modelling has some limits which should be kept in mind, due to the characteristics of the test section (geometrical and range of parameters) and to the features of the flow:

- *assessments and improvements* of certain correlations due to the SULTAN domain: low pressure (1 bar to 5 bar), large hydraulic diameter, small mass inlet velocity, inclined heated surface of large dimension. This will lead to improve the Net Vapor Generation Point, the CHF, the interfacial and the wall friction and the condensation in case of subcooling. At present, considering the modification of the NVG point correlation proposed by H. Nehme [12], the 1-D approach gives satisfactory results (good qualitative description of the main phenomena occurring in the test section) as far as the flow is mainly one-dimensional [13].

- Certain SULTAN observations have pointed out the presence of multi-dimensional effects, i.e. recirculation area as well as the simultaneous presence of 2 layers, a subcooled single-phase layer and a saturated 2-phase layer. The slip between the two layers can be taken into account by introducing a corrective factor in the interfacial
friction correlation, whereas the presence of a stagnation zone and a recirculation area will be drastic limits to the 1-D approach.

- **The 3-D approach** is based on the 3-D modelling of the test section. The objectives is to handle a tool "as simple as possible", able to take into account the multi-dimensional effects of the flow. It will be used to confirm the 1-D results or the presumptions questioning the presence of multi-dimensional effects.

  - the **main phenomena** to describe are the followings :
    - *wall transfers*, i.e. wall friction and heat flux correlations
    - *void fraction profile predictions*, i.e. the interfacial forces, the dispersion due to turbulence, the recondensation of saturated vapour in subcooled liquid
    - the *turbulence transfer*, of energy and of momentum type. They are modelled by means of a turbulent diffusivity, derived from a \((k,\varepsilon)\) model type (monophasic model which can be applied on both phases)
    - *prediction of the recirculation area*
  
  - nevertheless **some limits** have to be considered :
    - the state of the art of turbulence modelling, taking into account the fact that the domain is 2-phase flow, 3-dimensional and that the whole range of void fraction must be covered (all types of flow). Therefore, at present, the \((k,\varepsilon)\) turbulence model is implemented, which can be activated for any phase.
    - a certain "industrial constraint" has to be taken into account, i.e. considering the CPU time constraints, the complex physical phenomena and the lack of very fine measurements in the test section (especially concerning the phase velocity). the calculations are carried out with a rather coarse meshing for the test section.

**PRESENTATION OF A SULTAN 3-D CALCULATION**

**Test section modelling**

The 3-D calculation is carried out with the CATHARE 2 code version 1.4E revision 5.

As the flow is assumed to be mainly 2-dimensional, the test section meshing is limited to a 2-D meshing, with only one mesh in the width. Taking into account the limits of the 3-D approach, it yields a \(10 \times 1 \times 42\) meshes, in cartesian coordinates (see fig.7), the mesh cell being very small close to both walls (heated and adiabatic) and expanding towards the centre-line of the test section.

The \((k,\varepsilon)\) model is activated for the liquid phase. The imposed inlet turbulence level represents a slightly turbulent flow in a tube \((v = 9.10^{-4})\) where the turbulence level and the flow profiles are still not totally established. Indeed numerical tests have shown that the results of the simulation are only slightly dependant on the inlet imposed turbulence level.

**CATHARE 3-D module**

The CATHARE 3-D module [14] is based on a 2-fluid 6-equation model.
The discretization is based on a finite-volume method, structured mesh. It is of first order in space and time. It is based on the donor cell principle and staggered mesh grid. The numerical method is semi-implicit.

The physical relationships are extrapolated from those of the CATHARE 1-D module and extended in the three directions.

**Test conditions**

The selected test is a complex one which combines recirculation area, pre-heating of injected water to saturation and resulting evaporation process.

The test condition are the followings:

- vertical configuration
- gap width = 0.15 m
- outlet pressure $P_{\text{out}} = 5$ bar
- cooling inlet temperature $T_{\text{q,in}} = 100^\circ\text{C}$ ($\Delta T_{\text{sat}} = 50$ K)
- heat flux $\phi_{\text{wall}} = 470$ kW/m²
- inlet mass flowrate $G_{\text{q,in}} = 42$ kg/m²s

**CATHARE results**

All the CATHARE results, analysed and compared to experimental data, are steady-state data. The analysis is based on velocity fields (gas and liquid), void fraction and temperature fields and radial profiles, as well as axial profiles for the temperature.

- **liquid velocity field**
  - The results are drawn on figure 8.
  - The flow is accelerated in the vicinity of the heated wall, whereas the liquid velocity is nearly null close to the adiabatic wall.
  - In the lower part of the test section, the natural convection is the driving force. In the upper part of the test section, the water is even more accelerated which results in a recirculation region close to the upper part of the adiabatic wall.

- **gas velocity field**
  - The results are drawn on figure 9.
  - As soon as the evaporation starts, the gas velocity is accelerated, mainly due to buoyancy forces.
  - Because of bubble diffusion by turbulence, an effective horizontal mixing is observed. Due to the convection process, vapour at saturation is transported into subcooled liquid region. This leads to vapour recondensation.
  - It can be observed that the gas recirculation zone is less extended than the liquid one.

- **Void fraction field and radial profile**
  - The results are drawn on figures 10, 11.
  - As subcooled liquid is injected, evaporation starts within a considerable margin from the heated wall leading edge. As soon as the onset of evaporation occurs, the void fraction increases strongly with a significant vertical gradient then tends to be homogeneous vertically as well as horizontally.
The horizontal transport of gas bubble is mainly due to diffusion and recirculation.

Comparing the experimental and calculated radial profiles, it is observed that CATHARE does not perfectly predict the profiles but the main trends are correct. Indeed, at 2 meters high, a thin 2-phase layer is predicted as observed experimentally. At 4 meters high, the spreading of $\alpha$ with an asymmetrical profile is predicted but the average value is overestimated by CATHARE.

- **Liquid temperature field and profiles**
  - The results are drawn on figures 12, 13.
  - The liquid temperature increases almost nearly till saturation is reached.
  - The CATHARE and experimental radial profiles show a very good agreement. In the liquid zone (at 1 and 2 meters high), the temperature profiles are well predicted. In the upper part (3 and 4 meters high), the liquid is nearly-saturated due to recirculation.

**CONCLUSIONS AND PERSPECTIVES**

SULTAN program supported by CEA, EdF and FRAMATOME, has provided a great amount of data concerning CHF, pressure drops and local measurements of 2-D two-phase flow on a large scale experiment. The main phenomena of flow have been analyzed, together with the influence of thermohydraulic and geometric parameters. A more complete interpretation is still under progress.

Some SULTAN experiments have been calculated then analysed with the thermalhydraulic system code CATHARE. Two approaches have been followed simultaneously: a 1-D and a 3-D approach.

The 1-D approach provides a simple and efficient tool. It can be improved by adjusting the interfacial friction and the condensation. Nevertheless it will remain limited when multi-dimensional effects as recirculation are dominant.

The 3-D modelling is already capable of revealing such multi-D effects. Nevertheless its application has to be extended to inclined and nearly-horizontal tests. The accuracy of its prediction might be improved by further modelling developments, as an improved formulation for the bubble diffusion based on the interfacial forces (drag, buoyancy, lift ...). Also the addition of an interfacial area transport equation is discussed.

The experimental results indicate favourable possibilities of the coolability of a reactor vessel under natural convection. But it will have to be confirmed by validated CATHARE code calculation applied on realistic geometrical configuration and boundary conditions.

**BIBLIOGRAPHY**


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(7) R.W. Bowring, *A simple but accurate round tube uniform heat flux, dryout correlation over the pressure range 0.7 - 17 MN/m² (100 - 2500 psia)*, AEEW-R-789, 1972.


SULTAN: Schematic Diagram

SULTAN Instrumentation

SULTAN: Two Phase Flow Development

SULTAN: Profiles of Void Fraction and Temperature for vertical positions

FIGURE 1

FIGURE 2

FIGURE 3
Parameter Flux:
0 : 1000 kW/m²
1 : 900 kW/m²
2 : 800 kW/m²
3 : 700 kW/m²
4 : 600 kW/m²
5 : 500 kW/m²
6 : 400 kW/m²
7 : 300 kW/m²
8 : 200 kW/m²
9 : 100 kW/m²

Scales:
X:0.-→1500 kg/s/m²
Y:0.-→50000 Pa

Pressure Drop in Test Section (Pa) vs Mass Flow Velocity (kg/s/m²)

Campaign n° 1
Inclination : 90 Deg
Gap : .03 m
Outlet P : .1 MPa
Inlet subcooling : 50 °C

FIGURE 4

Parameter Flux:
0 : 1000 kW/m²
1 : 900 kW/m²
2 : 800 kW/m²
3 : 700 kW/m²
4 : 600 kW/m²
5 : 500 kW/m²
6 : 400 kW/m²
7 : 300 kW/m²
8 : 200 kW/m²
9 : 100 kW/m²

Scales:
X:0.-→4000 kg/s/m²
Y:0.-→25000 Pa

Pressure Drop in Test Section (Pa) vs Mass Flow Velocity (kg/s/m²)

Campaign n° 6
Inclination : 10 Deg
Gap : .03 m
Outlet P : .1 MPa
Inlet subcooling : 0 °C

FIGURE 5
FIGURE 6: 1-D extension to real geometry

FIGURE 7: SULTAN test section 3-D modelling

FIGURE 8: Velocity field liquid phase

FIGURE 9: Velocity field gas phase
FIGURE 10: Void-fraction distribution

FIGURE 11: Void fraction horizontal profiles, 2 m and 4 m

FIGURE 12: Liquid temperature distribution

FIGURE 13: Liquid temperature horizontal profiles, 1 m and 2 m
INTRODUCTION

External reactor vessel cooling (ERVC) is a severe accident management strategy that involves flooding the reactor cavity to submerge the reactor vessel in an attempt to cool core debris that has relocated to the lower head. Advanced and existing light water reactors (LWRs) are considering ERVC as an accident management strategy for in-vessel retention (IVR) of relocated debris. In the probabilistic risk assessment (PRA) for the AP600 design, Westinghouse credits ERVC for preventing vessel failure during postulated severe accidents with successful reactor coolant system (RCS) depressurization and reactor cavity flooding. To support Westinghouse’s position on IVR, the Department of Energy (DOE) contracted the University of California - Santa Barbara (UCSB) to produce a peer-reviewed report, DOE/ID-10460.

As part of their evaluation of IVR for the AP600, the Nuclear Regulatory Commission (NRC) tasked the Idaho National Engineering and Environmental Laboratory (INEEL) to perform an in-depth review of the UCSB study and calculation model, its peer review comments, and UCSB’s resolution method to identify any areas where technical concerns weren’t addressed. In addition, INEEL was tasked to perform an independent analysis effort to investigate the impact of residual concerns and parameter uncertainties on the margins to failure and conclusions presented in the UCSB study. This paper summarizes results from INEEL’s review, which is documented in Reference 3.

BACKGROUND

UCSB Study

To assess the effectiveness of ERVC for a depressurized AP600-like design, the UCSB study attempted to prove two assertions:

Assertion 1: For all heat fluxes at or below the critical heat flux (CHF), the corresponding minimum vessel wall thicknesses are sufficient that the vessel remains intact.

Assertion 2: Heat fluxes from relocated melt to the lower head always remain below CHF.

The UCSB study relied on various types of analyses, experiments, and assumptions to support each assertion. They applied CHF data from UCSB ULPU tests to estimate minimum vessel thicknesses for structural analyses and provide bounding heat fluxes for thermal analyses. For the minimum vessel wall thicknesses, structural calculations demonstrated that the vessel remains intact if heat fluxes are at or below values assumed for CHF (Assertion 1). Thermal analyses and data from UCSB experiments provided a basis for estimating maximum possible heat fluxes from debris configurations that were assumed to “bound” the thermal loads from all other debris configurations that can “reasonably be expected.” These heat fluxes (and associated uncertainties) were below values assumed for CHF (Assertion 2). Hence, the UCSB study concluded that “thermally-induced failure of an externally flooded, AP600-like reactor vessel is physically unreasonable.”
Peer Review Comments

Seventeen international experts reviewed selected sections of the UCSB study. This peer review process included an initial and a final round of comments to ensure that initial comments were adequately addressed. Peer review comments spanned a range of concerns including the adequacy of uncertainty distribution assumptions, the potential for more challenging debris configurations, ceramic layer decay heat loads, the potential for metallic layer heat sources, material property assumptions, the potential for partially flooded cavities, and the validity of assumed heat transfer correlations. Typically, the UCSB study addressed peer review comments by performing point estimate sensitivity studies in which one parameter was varied from its base case value or with a limited number of other parameters. Although these sensitivity studies provide insights about the impact of the varied parameter, they don’t reveal integral effects of changes suggested by peer reviewers.

As part of INEEL’s review, peer review comments that collectively have the potential to impact vessel response were identified. Table 1 summarizes these comments by topic. The second column contains a paraphrased version of the comment, and the third column list the number of comments on this topic and the number of reviewers with these comments. As indicated in this table, INEEL’s review indicated that several comments from various reviewers required additional consideration. A more detailed version of this table listing the specific reviewer comment, the approach taken in the UCSB study for resolution, and INEEL’s approach for resolution may be found in Reference 3. In general, INEEL addressed the residual concerns associated with the comments listed in Table 1 by performing independent calculations that considered the combined integral effects associated with uncertainties of various input assumptions.

It should be noted that the comments in Table 1 reflect INEEL’s view of outstanding items that required additional attention. In some cases, INEEL judged that a peer review comment about an issue wasn’t adequately addressed although the peer reviewer may not have noted that the comment was an outstanding item in their final comments. In other cases, INEEL may agree with a reviewer that the comment wasn’t adequately addressed, but INEEL omitted the comment from this table because INEEL judged the issue did not significantly impact vessel response.

<table>
<thead>
<tr>
<th>Topic</th>
<th>Paraphrased Comment</th>
<th>Number</th>
</tr>
</thead>
<tbody>
<tr>
<td>Uncertainty Distributions</td>
<td>Broader uncertainty distributions for various input parameters should be used.</td>
<td>10 comments from 4 reviewers</td>
</tr>
<tr>
<td>Alternate Debris Configurations/Intermediate Debris States</td>
<td>Several credible alternate debris configurations or intermediate states may be more challenging.</td>
<td>18 comments from 9 reviewers</td>
</tr>
<tr>
<td>Metallic Layer</td>
<td>Metallic layer may contain volumetric heat sources from oxidation, fission product retention, or dissolved uranium.</td>
<td>7 comments from 4 reviewers</td>
</tr>
<tr>
<td>Critical Heat Flux</td>
<td>Assumed emissivity from the metallic layer was too high.</td>
<td>27 comments from 8 reviewers</td>
</tr>
<tr>
<td>Decay Heat Load</td>
<td>The applicability of ULPU data and the impact of various phenomena on CHF should be evaluated.</td>
<td>4 comments from 2 reviewers</td>
</tr>
<tr>
<td>Molten Pool Natural Convection</td>
<td>Higher decay heat loads may occur.</td>
<td>4 comments from 2 reviewers</td>
</tr>
<tr>
<td>Vessel Wall Melting Temperature</td>
<td>Prototypic material test results are needed to confirm the applicability of ACOPO data.</td>
<td>3 comments from 2 reviewers</td>
</tr>
<tr>
<td>Ceramic Pool Liquidus Temperature</td>
<td>Higher heat fluxes may occur at locations where CHF values are lower during transient time periods.</td>
<td>3 comments from 3 reviewers</td>
</tr>
<tr>
<td></td>
<td>Vapor from lower boiling point, metallic materials would rise up through oxidic pool and enhance heat transfer.</td>
<td>3 comments from 2 reviewers</td>
</tr>
<tr>
<td></td>
<td>Range of possible eutectic temperatures wasn’t evaluated.</td>
<td>2 comments from 2 reviewers</td>
</tr>
<tr>
<td></td>
<td>Other ceramic pool liquidus temperatures may occur.</td>
<td></td>
</tr>
</tbody>
</table>
Debris Configuration

A key UCSB study assumption is their “bounding” debris configuration, which they refer to as the “Final Bounding State” or “FIBS”. The UCSB FIBS assumes a molten ceramic pool lies beneath a metallic layer (Figure 1). The ceramic pool contains oxidic core components (mainly UO₂ and ZrO₂). Turbulent steady-state natural convection associated with volumetric heat sources governs pool heat transfer. The molten pool experiences sufficient cooling that it is surrounded by thin crusts imposing uniform temperature boundary conditions at the outer pool surface. The thin metallic layer is heated from below and cooled from above and its sides. The side boundary temperature is fixed at the metallic layer liquidus. Several peer reviewers commented that the UCSB-assumed FIBS is not necessarily the most bounding or plausible debris configuration. Based on peer reviewer comments, experimental results, and severe accident code calculation results, INEEL defined three other configurations that may be more challenging than the UCSB-assumed FIBS (see Figure 1).

Configuration A. Configuration A is similar to the UCSB-assumed FIBS, but evaluated at an earlier time period, before all the metallic and ceramic material relocates. Based on SCDAP/RELAP5 calculation results, Configuration A contains a large oxidic pool (~50% of the core inventory which corresponds to ~37,000 kg of UO₂ and ~4,000 kg of ZrO₂) with a small metallic component (~3800 kg of unoxidized zircaloy and ~2600 kg of stainless steel). This configuration developed primarily from a series of localized relocations through the reflector sidewall. The stainless steel component is associated with the melting of gray rods and the addition of lower plenum structures that melted as a result of being submerged by the debris. Calculations suggesting that this configuration could persist for more than an hour indicate that it is appropriate to consider this intermediate configuration as a “quasi-steady” state.

![Diagram of configurations](image)

(a) UCSB FIBS.

(b) INEEL alternate debris configurations.

Figure 1. UCSB FIBS and INEEL alternate debris configurations.
Configuration B. Configuration B is based on the SCDAP/RELAP5 calculations that formed the basis for Configuration A. At a time after Configuration A develops, SCDAP/RELAP5 predicts that an additional 35% of the core materials (an additional ~27,000 kg of UO₂ and ~3,000 kg of ZrO₂) relocates to form a second molten pool. The metallic layer from the first relocation would be heated from below by the molten pool and above by the overlying pool and crust.

Configuration C. Based on experimental data suggesting that some uranium will dissolve into unoxidized zirconium,⁵ Configuration C represents a case where a more dense U-Zr metallic layer sinks below the oxidic pool. Heat sources within the lower metallic layer are focussed toward the bottom of the vessel where CHF values are lowest.

Relocation masses for the above configurations were based on SCDAP/RELAP5 results. However, it should be noted that SCDAP/RELAP5 does not predict stratified debris configurations. Because there are no experimental data conclusively proving that density differences overcome turbulent convective currents in relocated debris, SCDAP/RELAP5 doesn’t assume that density differences cause melt segregation to occur. Materials segregation was assumed to be consistent with UCSB FIBS assumptions and allow comparable calculations to be performed.

INEEL INDEPENDENT ANALYSES

VESTA
To independently verify UCSB study results and assess the impact of additional uncertainties and other debris configurations, INEEL developed the VESSEL Statistical Thermal Analysis (VESTA) code. VESTA and the UCSB model include the following heat transfer processes:

- steady-state turbulent natural convection within a volumetrically-heated molten ceramic pool;
- convection within the metallic layer;
- conduction through molten pool ceramic crust, upper plenum structures, and the vessel;
- radiation heat transfer from the metallic layer upper surface to the upper plenum structure inner surface and the upper plenum structure outer surface to the vessel;
- boiling heat transfer from the vessel outer surface.

VESTA and the UCSB model compare vessel wall heat fluxes with critical heat flux correlations. In VESTA, Bayesian uncertainty distributions are combined by a Monte Carlo sampling to yield a distribution on the probability of vessel heat fluxes exceeding CHF.

VESTA allows users to address unresolved peer review concerns about UCSB model limitations by considering:

- Selected alternate debris configurations (users may select either Configuration A, B, or C);
- Decay heat power production associated with actinide and fission product heating;
- Metallic layer heat sources based on the fraction of actinide or fission product decay heat and/or other heat sources;
- Material property uncertainties and dependencies on temperature and/or composition;
- Various decay heat loads and associated uncertainties;
- Various metallic layer heat transfer correlations and associated uncertainties;
- Various molten pool heat transfer correlations and associated uncertainties;
- Various CHF correlations and associated uncertainties;
- Various input parameter distributions (normal, log normal, Student’s t, uniform, user-specified, or point estimate).

UCSB FIBS
For the UCSB-assumed FIBS calculations, INEEL performed two types of calculations: a verification analysis assuming UCSB input and a case with INEEL input.

UCSB Input. To verify UCSB study results and the equations listed in the UCSB study were correctly encoded into VESTA, INEEL first assumed UCSB input for evaluating the UCSB-assumed FIBS. Figure 2 compares CHF probability distribution functions (pdfs) predicted by VESTA (designated INEEL - UCSB input) and the UCSB model (designated UCSB) at selected locations along the vessel lower head. As shown in Figure 2, VESTA and UCSB model predictions are similar.

INEEL Input. Reference 3 documents INEEL’s approach for revising the UCSB input distributions. Significant differences in input used by INEEL include:

- Replacing UCSB 1/8th-scale Mini-ACOPO molten pool natural convection heat transfer correlations with correlations that INEEL derived using UCSB 1/2-scale ACOPO data;
Figure 2. UCSB and VESTA (INEEL-UCSB input) results for UCSB FIBS with UCSB input distributions.

- Replacing UCSB ULPU CHF correlation with Pennsylvania State University (Penn State) Subscale Boundary Layer Boiling (SBLB) CHF correlations;
- Assuming appropriate uncertainties in heat transfer correlations and decay power curves;
- Assuming a metallic layer heat source based on the fraction of fission products present;
- Basing melt relocation times on severe accident analysis code predictions;
- Basing material properties and uncertainties on a wider range of published experimental data.

This paper discusses two examples illustrating the manner in which INEEL modified input distributions.

**Metallic Layer Emissivity.** In the UCSB-assumed FIBS, the metallic layer is composed of unoxidized zirconium and stainless steel. If the metallic layer were oxidized, it isn’t clear if heat loads to the vessel would be reduced. Enhanced upward heat losses due to radiation from the oxidized layer could be offset by the oxide layer’s conduction resistance, oxidation energy into the metallic layer, and reduced heat losses from a dense fog associated with water vaporizing in the RCS. Although the UCSB authors acknowledged that there is the possibility of an oxidic “film” on top of the metallic layer, they assumed an unoxidized metallic layer in their FIBS analysis and treated some of the effects associated with an oxidized metallic layer in a point estimate sensitivity study.

In their FIBS analysis, the UCSB study assumed that the unoxidized zircaloy and stainless steel metallic layer had an emissivity of 0.45 with no uncertainty. This value was based on data from UCSB tests using previously oxidized, primarily carbon steel materials. Figure 3 compares UCSB emissivity data with molten zirconium and unoxidized solid stainless steel and zirconium emissivity data from References 6 through 8. Because UCSB measurements for the previously oxidized, primarily carbon steel, material appear higher than data from various references for stainless steel and zirconium materials, INEEL estimated the metallic layer emissivity and its uncertainty distribution using Figure 3 zirconium and stainless steel data (the four non-UCSB groups of data points in Figure 3).

![Figure 3. Comparison of UCSB emissivity data with unoxidized stainless steel and zirconium data.](image-url)
CHF Correlation. The UCSB study assumed an ULPU "lower bound" correlation with no uncertainty. In Reference 9, Cheung derives a scaling relationship from the NRC-sponsored SBLB tests that predicts CHF as function of several parameters, such as coolant pressure, local coolant subcooling (associated with gravity head), coolant velocity, vessel size, and position. Figure 4 plots this scaling relationship applied to an AP600-like reactor vessel. Three cases are shown to reflect the difference in critical heat flux due to local coolant subcooling associated with gravity head (denoted by the reactor cavity coolant height to vessel radius ratio, H/R). As shown in Figure 4, the critical heat flux increases with coolant height (because of increased subcooling associated with coolant head). To select an appropriate CHF correlation, INEEL compared time-dependent AP600 cavity water levels with molten pool formation times estimated by SCDAP/RELAPS, MELCOR, and MAAP4. The ratio of the water height to the vessel radius varies between ~2 and 4.4 (the maximum flooding height) for time periods when these references predict molten pools occur in the lower head. Hence, INEEL assumed the (H/R=3) Cheung correlation and associated uncertainties.

Results. Figure 5 compares point estimates for vessel heat flux assuming INEEL input (the INEEL curve) with the UCSB input (the DOE/ID-10460 curve). CHF ratio pdfs predicted with INEEL input distributions are plotted in Figure 6. As shown in these figures, INEEL input increases heat flux and CHF ratio predictions at vessel locations in contact with the metallic layer (at angles greater than ~76.2°). This result is primarily due to modeling metallic layer heat sources, reducing metallic layer emissivity, and assuming Cheung SBLB CHF correlations. Although INEEL assumed increased ceramic pool decay heat, this increase is offset by other modifications that reduce ceramic layer heat fluxes. Finally, pdfs are less peaked because additional input parameter uncertainties are considered.

Figure 4. Comparison of ULPU and SBLB correlations.

Figure 5. Comparison of heat fluxes for UCSB FIBS.

Sensitivity Analyses

Although INEEL VESTA calculations indicate that CHF ratios remain below unity for the UCSB FIBS, several phenomenological uncertainties identified by peer reviewers preclude conclusive definition of several input uncertainty distributions. INEEL sensitivity calculations suggest that two phenomenological uncertainties, vapor-enhanced upward heat transfer and reduced metallic layer mass, may lead to non-negligible failure probabilities. Note that INEEL sensitivity studies look at the integral effect of...
Figure 6. CHF ratio pdfs for UCSB FIBS with INEEL input distributions.

Varying an assumed input parameter distribution or modeling assumption. In these calculations, INEEL input parameter uncertainty distributions for the UCSB FIBS were assumed except for the parameter(s) and/or equations that were modified to assess the phenomena of interest.

Vapor-Enhanced Upward Heat Transfer. As observed by several peer reviewers, if the temperature in the ceramic pool exceeds the boiling temperature of one or more metallic components within the ceramic pool, vaporization and pool boiling may occur, enhancing upward heat transfer from the molten pool. The presence of such lower vaporization temperature materials is supported by voids measured in debris from the Three Mile Island Unit 2 (TMI-2) vessel and several severe fuel damage tests. Although it is recognized that mechanisms, such as melting upper plenum metallic structures, introducing metallics into the pool would eventually cease as debris decay heat decreases, this transient state may exist for sufficient times that it is appropriate to characterize it as a quasi-steady state.

Data for characterizing heat transfer from volumetrically-heated boiling pools suggest that upward and sideward heat losses on upper portions of the pool may increase orders of magnitude for pool Rayleigh numbers between $10^{14}$ and $10^{17}$ and pool void fractions between 0.1 and 0.5. INEEL assumed a factor of 10 increase in the upward heat transfer correlation to characterize the increase in heat transfer due to vaporization and boiling. For this case, Figure 7 heat fluxes at vessel locations in contact with the metallic layer (at angles above $76.2^\circ$) are nearly 63% higher than values estimated in Figure 6. These increased heat fluxes are due to increased heat input from the ceramic pool into the metallic layer. Figure 7 results indicate that 0.13% of the CHF ratios at locations adjacent to the metallic layer exceed unity; or the probability of vessel heat fluxes exceeding CHF is 0.13%, i.e.,

$$
\int \text{pdf}(q''(\theta)/q''_{\text{CHF}}(\theta)) \, dq''(\theta)/q''_{\text{CHF}}(\theta) = 0.0013.
$$

Figure 7. CHF ratio pdfs assuming increased upward ceramic pool heat losses.

Metallic Layer Steel Mass. For their FIBS, the UCSB study assumed a steel melt mass pdf that primarily ranged from 67,000 to 77,000 kg. The minimum value in this pdf corresponds to all the core plate (~25,000 kg), all the reflector (~40,000 kg), and all the lower internal structures (~2,000 kg) forming the metallic layer. The UCSB study qualitatively argues that it is not possible to get substantial relocation of ceramic material without relocating the core.
plate and reflector because the massive steel reflector and core plate delay ceramic material relocation. However, several peer reviewers questioned UCSB’s assumed metallic layer mass pdf, observing that the UCSB FIBS doesn’t necessarily bound challenges from intermediate debris states. Many severe accident analysis codes contain models based on post-accident TMI-2 examinations that allow localized reflector and/or core plate failure. In addition to the Reference 3 SCDA/RELAP5 calculations, Reference 16 MELCOR calculations, Reference 17 MAAP calculations, and Reference 18 reviewers estimate smaller AP600 steel relocation masses (~6,000-27,000 kg) than the UCSB minimum mass.

Larger metallic masses result in lower vessel wall heat loads because metallic layer heat is dissipated over larger surface areas. The large impact of steel melt mass assumptions suggests that a single pdf considering the entire range of postulated melt masses possible at various times in the transient would not yield meaningful results. Hence, INEEL performed a series of VESTA point estimate calculations in which the metallic mass was reduced. Results indicate that CHF ratios at vessel locations in contact with the metallic layer approach unity for steel melt masses equal to 19,500 kg. Uncertainty distributions in Figure 8 suggest that this less than a factor of four reduction in steel mass results in a 52% probability of exceeding CHF at angles of 85°.

Alternate Debris Configurations. VESTA was also applied to consider thermal challenges presented by the three Figure 1 alternate debris configurations. VESTA allows each configuration to be analyzed by simply modifying a select number of input parameter distributions: each layer’s mass of relocated metal and ceramic materials, each layer’s oxidation fraction of zirconium and uranium, the decay heat, the amount of decay heat produced by actinides, the metallic layer emissivity, and CHF correlation coefficients. Metal and ceramic material masses, zirconium oxidation fractions, melt relocation time, and debris decay heat assumptions (power and fraction produced by actinides) were based on a SCDA/RELAP5 AP600 3BE analysis. Metallic melt emissivity and CHF correlation coefficients were selected to reflect conditions expected at time periods when intermediate configurations occur. Specifically, INEEL assumed a lower metallic melt emissivity to reflect the presence of steam and the Cheung (H/R=2) CHF curve to reflect time periods before the reactor cavity becomes fully flooded. For Configuration C, the uranium oxidation fraction and lower layer unoxidized uranium mass were selected to allow a more dense, lower metallic layer and stay within possible U-Zr weight fractions.

VESTA results indicate that the UCSB FIBS doesn’t bound possible heat loads. VESTA calculations for configurations A, B, and C indicate that peak heat fluxes occur at locations where the vessel is in contact with the metallic layer. Configuration A uncertainty calculation results in Figure 9 indicate that the probability of vessel heat fluxes exceeding CHF is approximately 85% at locations where the vessel is in contact with the metallic layer. These higher values are primarily due to the reduced metallic layer mass, the reduced metallic layer emissivity, and higher decay heats (associated with earlier relocation times). Metallic layer heat fluxes for Configurations B and C are predicted to exceed CHF. In Configuration B, this result is due to the combined heat load from the upper and lower ceramic pools; whereas in Configuration C, this result is due to high metallic layer heat fluxes (due to retained fission products and actinides) and lower CHF values occurring near the lower metallic layer (see Figure 4).

It should be noted that VESTA (or UCSB model) heat flux predictions exceeding CHF are beyond the range of
applicability for these models. In a detailed transient analysis, heat fluxes above CHF would cause vessel temperatures to rise to values where the steel loses its strength, experiencing creep structural instability. Because governing equations in VESTA (and the UCSB model) are limited to cases where heat fluxes are below CHF, heat flux predictions exceeding CHF should merely be viewed as an indicator of vessel failure.

CONCLUSIONS

In summary, INEEL found that there were several peer reviewer comments that were not fully addressed by the JCSB study. INEEL performed independent calculations to resolve residual peer reviewer concerns. These calculations provided the following insights:

- Existing data don't support some UCSB-assumed material property assumptions;
- Metallic layer fission product decay heat is neglected;
- UCSB relocation times and associated decay heats differ from existing standards and severe accident code predictions;
- Heat transfer correlation, decay heat, and some material property uncertainties are neglected.

Sensitivity calculations for the UCSB FIBS indicate that certain phenomenological uncertainties significantly impact vessel heat flux estimates. Specifically,
- increased upward heat fluxes associated with vaporization and boiling of lower vaporization temperature materials may increase vessel heat flux estimates by 63%;
- less than a factor of four decrease in relocated steel mass results in a 52% probability of vessel heat fluxes exceeding CHF.

VESTA alternate configuration analyses indicate:
- The UCSB-assumed FIBS doesn't bound possible heat loads;
- Vessel heat fluxes associated with intermediate states (Configurations A and B) and a lower metallic layer (Configuration C) exceed CHF.

ACKNOWLEDGMENTS

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In-vessel core melt retention by RPV external cooling for high power PWR. MAAP 4 analysis on a LBLOCA scenario without SI.

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1. Introduction

In-, ex-vessel reflooding or both simultaneously can be envisaged as Accident Management Measures to stop a Severe Accident (SA) in vessel. This paper addresses the possibility of in-vessel core melt retention by RPV external flooding for a high power PWR (4250 MWh). The reactor vessel is assumed to have no lower head penetration and thermal insulation is neglected. The effects of external cooling of high power density debris, where the margin for such a strategy is low, are investigated with the MAAP4 code.

MAAP4 code is used to verify the system capability to flood the reactor pit and to predict simultaneously the corium relocation into the lower head with the thermal and mechanical response of the RPV in transient conditions. The corium pool cooling and holding in the RPV lower head is analysed. Attention is paid to the internal heat exchanges between corium components. This paper focuses particularly the heat transfer between oxidic and metallic phases as well as between the molten metallic phase and the RPV wall of utmost importance for challenging the RPV integrity in vicinity of the metallic phase.

The metal segregation has a decisive influence upon the attack of the vessel wall due to a very strong peaking of the lateral flux ("focusing effect"). Thus, the dynamics of the formation of the metallic layer characterized by a growing inventory of steel, both from a partial vessel ablation and the degradation of internals steel structures by the radiative heat flux from the debris, is displayed.

The analysed sequence is a surge line rupture near the hot leg (LBLOCA) leading to the fastest accident progression.

2. Presentation of the models

2.1 General features of MAAP4 [1]

MAAP4 is an integrated tool used to predict the progression of severe accident scenarios in PWRs both in the NSSS (Nuclear Steam Supply System) and in the containment. The code describes accident sequences initiated by a set of events defined by the user and leading to either a safe and stable state or to a loss of Primary System integrity and in worst cases to a containment failure. In addition to usual thermohydraulic calculation, a large number of SA phenomena (Fig. 1) are tackled by MAAP4 such as :

- core heatup, degradation and relocation into the lower head ;
- thermal and mechanical response of the RPV ;
- \( H_2 \) production, transport, distribution and combustion ;
- MCCI (Molten Core Concrete Interaction) ;

- DCH (Direct Containment Heating).

The code also addresses engineered safety systems and allows the user to model automatic actions or operator interventions.

2.2 Debris bed dynamics and energy transport in lower plenum

After large molten pool formation into the core, two possible corium relocation processes are considered by the code :

- sideward by melting of the core barrel/baffle,
- downward by melting and mechanical failure of the core support plate.

During this process, MAAP4 calculates the interactions between the core debris and the RPV wall as shown in Figure 1. When the debris settle down at the bottom of the lower plenum, two types of debris are considered. One is a particulate debris bed and the other is a continuum bed. The continuum bed is divided into the central molten pool, peripheral debris crusts and overlying metal. Figure 1 illustrates the debris bed configurations and the thermal interaction modeled.

![Physical Phenomena tackled by MAAP4](image)

**Fig.1. Physical Phenomena tackled by MAAP4**

2.2.1 Major assumptions

1. Continuum debris bed

- Metal and oxides are separated due to density differences; the metallic part forms a continuous layer on the upper surface of the region.
- Debris enter this region from melting in the particulated region or upon ablation of RPV wall and lower plenum
structures.
-Crusts are formed on the RPV wall and upper surface of the region. The debris crusts have the same composition as the debris pool.
-Temperatures of the oxidic pool and the metallic layer are function of time but not of space since intensive mixing during the strong turbulent natural circulation is assumed.
-The steady state values of the heat transfer coefficients are reached simultaneously.
-Part of the radiation energy emitted from the free surface of the pool is consumed for melting the surrounding metallic structures.

2. Particulate debris bed
-Material is added to this region by jet particulation (debris jet drops into the water filled lower plenum) and by the collapse of solid structures from above.
-Material leaves this region by melting and is added to the continuum bed below.
-The particulate debris bed, which is a mixture of metal and oxide, has not crust and is characterized by particulate diameter and porosity to calculate its cooling rate.
-The particulates are allowed to submerge in the metallic layer with their decay heat being directly added to the layer.
-Heat removal is calculated by convection and radiation.

Using these governing principles, the balances for heat and mass transfer can be formulated for each type of debris condition in the lower plenum. With the integration of the individual rates, the formation, growth and possible shrinkage of these regions can be calculated.

2.2.2 Heat flux correlations

Steady state relationships are used to describe the heat transfer rates with the assessment of solid or liquid state.

Debris pool convection heat transfer

Natural convection heat transfer is calculated from the molten pool respectively to the upper and lower crusts. Experiments (with Joule-heated simulants in simple geometry) indicated a strong propensity for upward and lateral heat removal as opposed to that downwards in volumetrically heated geometry. This propensity is observed to increase as the Rayleigh number (RaH):

$$ \text{Ra}_H = \frac{g \beta \Delta T S^3}{\alpha \nu \lambda} \quad (1) $$

Thus, the approach adopted by MAAP4 is to represent the heat transfer coefficients by the Rayleigh number similar to those recommended by Epstein & Fauske [2], Mayinger et al. [3], Jahn et Reineke [4] and those experimentally observed by Kymalainen in the COPO facility [5].

The average upward and downward heat flux are expressed as:

$$ \text{Nu}_H = 0.345 \text{Ra}^{0.233} \quad \text{(Steinberger) (2)} $$

$$ \text{Nu}_d = 0.54 \text{Ra}^{0.18} \left( \frac{Z_{tp}}{R_{tp}} \right) \quad \text{(Mayinger) (3)} $$

These equations provide the convective heat transfer coefficients (h = \frac{\text{Nu} \lambda}{Z_{tp}}) for energy transfer from the central debris pool to the upper and lower crusts.

Azimuthally varying heat flux

The experimental studies of Jahn & Reineke [4] demonstrated that the local downward heat flux varies considerably along the boundary of the pool. The MAAP4 molten pool model includes an azimuthally varying heat transfer from the pool to the crusts.

Heat transfer through metallic layer

The presence of the metallic layer is likely to modify significantly heat transfer. If the metallic layer is determined to be molten, the heat transfer coefficient within the layer is given by an extension of the Globe-Dropkin correlation [6]. The Globe-Dropkin correlation for the Nusselt number is expressed as:

$$ \text{Nu} = 0.069 \text{Ra}^{3/4} \text{Pr}^{0.074} \quad 0.02 < \text{Pr} < 8.75, \quad 3.10^5 < \text{Ra} < 7.10^9 \quad (4) $$

with the Rayleigh number based on the temperature difference between lower and upper faces of the steel layer and defined as:

$$ \text{Ra} = \frac{g \beta \Delta T S^3}{\nu \alpha} \quad (5) $$

To combine the conduction and convection processes in the metallic layer, the Nusselt number is assumed to take the form:

$$ \text{Nu} = 1 + 0.069 \text{Ra}^{1/3} \text{Pr}^{0.074} $$

Heat transfer between the metallic layer and the vessel wall

The Churchill & Chu correlation [7] has been introduced in the code to assess the heat transfer between the metallic layer and the RPV wall (turbulent regime always considered):

$$ \text{Nu} = \frac{0.15}{1 + \left( 0.492 \frac{9}{\text{Pr}} \right)^{16}} \quad \text{Ra}^{3/4}, \quad \text{Gr} > 10^9, \quad 0.7 < \text{Pr} < 7 \quad (6) \text{ (Gr = grav. number)} $$

with the Rayleigh number based on the average temperature of the metallic layer and on the wall surface temperature and defined as:

$$ \text{Ra} = \frac{g \beta \Delta T S^3}{\nu \alpha} (T_S - T_{sw}) \quad (7) $$
At the beginning of relocation, the small aspect ratio of the metallic layer may lead to the formation of several distinct convective cells responsible for a radial temperature profile inside the metallic layer. A reduction of the heat flux imposed to the vessel wall could be expected. However, as no radial temperature profile is available, this potential reduction cannot be taken into account with the MAAP4 code. Besides, the free surface oxidation by steam (emissivity increase) playing a role on the radiation heat transfer is not considered here.

2.3 Heat transfer to water and reactor vessel internals

The upward losses from the debris bed are given by the combination of convection and radiation. Heat transfer to the top is assumed to be dominated by the radiation contribution. Radiation from the debris bed in the lower plenum goes to all exposed heat sink surfaces. If the lower plenum heat sink (core support plate) has been completely melted, the energy is assumed to be radiated to the surviving core bottom nodes. If the lower core nodes are melted out, the upward heat flux from the debris is likely to cause melting of other internals steel structures. This process will increase the metallic content of the debris and will have a profound effect on the heat transfer.

2.4 Thermal response of heat sinks (RPV wall)

The azimuthal heat flux from crust to RPV wall is used as a boundary condition for conduction calculation. Heat conduction in the vessel wall is transient, symmetric with respect to the vessel axis. In MAAP4, a primary heat sink is described as a two-dimensional slab with its inner and outer surfaces subjected to different thermal and material boundary conditions imposed by such surrounding area as core material, steel layer, water and gas. MAAP4 will calculate the process of core heatup and cooldown including liquefaction and resolidification. Given the nodalization, timesteps and boundary conditions, MAAP4 determines temperature rates of change of each node, average temperature, heat transfer area and heat transfer rates, liquified mass and residual thickness.

The RPV wall axial nodalization considered by MAAP4 is displayed in Fig. 2. Each axial node (from 1 to 10) is divided into 5 radial cells in the thickness of the wall. The RPV wall temperature distribution and the primary pressure are used to estimate challenges to RPV integrity. The Larson-Miller relationship (creep) is evaluated to determine the material rupture times as function of stress and temperature.

2.5 Heat transfer between RPV wall and external water

MAAP4 calculates the nucleate boiling heat transfer coefficient using Rohsenow's correlation whatever the RPV wall temperature level. The correlation is the following:

$$ h = \left( \frac{C_g \Delta T}{0.013 \rho_f \alpha_f} \right)^{1/3} $$

Heat transfer from RPV lower head outer surface to cavity water pool leads to water heatup and then to steaming. Any steam generated in cavity is removed from the reactor pit mass and added to the lower compartment steam mass. The assumed steam flow path is around the RPV annulus into the lower compartment.

![Fig. 2. RPV wall axial nodalization used in MAAP4. The elevations of the different corium components are given at the end of the transient.](image)

3. Description of the scenario

A possibility for external flooding is investigated: a passive solution is used for 5 hours, extended by a CHRS (Containment Heat Removal System) solution beyond. The typical scenario used to design the system is as follows:

- t=0 : initiating event
- t=1/2 h : core uncovering
- t=1 h : the first corium overflows reach the lower head
- t=3 h : complete corium relocation in the lower head

Within the scope of this study, aeroballs pool and upper internals storage pool are used to gravity fill the reactor pit. The storage capacity of those pools, with a total volume of about 700 m³, guarantees a passive flooding lasting 5 hours. After drainage of the 2 pools, the CHRS fulfills the flooding function by injecting directly into the reactor pit with a low...
flowrate \(= 75 \text{ m}^3/\text{h}\). Figure 3 illustrates the flooding system.

4. Analysed sequence

A surge line rupture at the hot leg nozzle location is analysed. The Safety Injection System (SIS) is unavailable but the steam generators are available. When the SIS setpoint is reached at low pressurizer pressure of 110 bars, a partial cooldown is initiated by an automatic opening of the SG relief valves; in 20 minutes, steam generators are depressurized to a secondary side pressure of 60 bar (RCS cooldown rate of 100 K/h). Then, no manual secondary side depressurization is considered; SG relief valves setpoint stays at 60 bar. Due to the size of the break, the RCS is rapidly depressurized to the containment pressure (in 13 minutes, \(P_{RCS} = P_{CONTAINMENT} = 3.2\) bar).

One hour after the reactor scram, gravity flooding is initiated with an initial flowrate of 53.6 kg/s. 5 hours after the beginning of the reactor pit flooding (when the storage capacities are empty), the CHRS system is switched on. Water is pumped from the IRWST (In Refueling Water Storage Tank) compartment and injected in the reactor pit. Injected mass flowrate in the reactor pit versus time is shown in Figure 4 for both phases.

5. Results

5.1 Key events

<table>
<thead>
<tr>
<th>KEY EVENTS TIMES</th>
<th>Time in h min s</th>
</tr>
</thead>
<tbody>
<tr>
<td>Hot leg break</td>
<td>0 s</td>
</tr>
<tr>
<td>Reactor scram</td>
<td>8 s</td>
</tr>
<tr>
<td>Main coolant pumps coast down</td>
<td>14 s</td>
</tr>
<tr>
<td>Start of partial cooldown</td>
<td>18 s</td>
</tr>
<tr>
<td>Accumulator water injection</td>
<td>1 min 33 s</td>
</tr>
<tr>
<td>Accumulator water depletion</td>
<td>2 min 33 s</td>
</tr>
<tr>
<td>Start of core uncovery</td>
<td>14 min</td>
</tr>
<tr>
<td>Start of core melting</td>
<td>28 min</td>
</tr>
<tr>
<td>Start of water injection into the reactor pit</td>
<td>1 h 00 min 08 s</td>
</tr>
<tr>
<td>Start of core relocation</td>
<td>1 h 30 min</td>
</tr>
<tr>
<td>Water level in reactor pit exceeds RPV loops level</td>
<td>2 h 04 min</td>
</tr>
<tr>
<td>Start of metallic layer melting</td>
<td>2 h 21 min</td>
</tr>
<tr>
<td>All the metallic layer melted</td>
<td>2 h 37 min</td>
</tr>
<tr>
<td>End of core relocation</td>
<td>3 h 44 min</td>
</tr>
<tr>
<td>Cavity water injection depleted - CHRS activation</td>
<td>6 h 00 min 08 s</td>
</tr>
</tbody>
</table>

5.2 Major results

<table>
<thead>
<tr>
<th>MAIN RESULTS</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial water mass flow rate from the internals pool</td>
<td>53.6 kg/s</td>
</tr>
<tr>
<td>Final water mass flow rate from the internals pool (at (t=6\ h\ 08\ s))</td>
<td>25.0 kg/s</td>
</tr>
<tr>
<td>CHRS water mass flow rate</td>
<td>20 kg/s</td>
</tr>
<tr>
<td>Final residual thickness of node # 5</td>
<td>2 cm</td>
</tr>
<tr>
<td>Final residual thickness of node # 6</td>
<td>9.8 cm</td>
</tr>
<tr>
<td>Final metallic layer aspect ratio</td>
<td>0.32</td>
</tr>
<tr>
<td>Final heat flux from the metallic layer to RPV wall</td>
<td>1.54 MW/m²</td>
</tr>
<tr>
<td>Final elevation of corium in the lower head</td>
<td>2.63 m</td>
</tr>
<tr>
<td>Final thickness of the metallic layer</td>
<td>0.78 m</td>
</tr>
<tr>
<td>Final mass of corium in lower head</td>
<td>296 t</td>
</tr>
<tr>
<td>Heat flux from metallic layer to RPV wall</td>
<td>1.46</td>
</tr>
</tbody>
</table>

5.3 Description of the transient

The continuum debris bed begins to form as the relocation from the core becomes more massive with the decreasing amount of water in the lower plenum. The upper crust which appears at the core relocation time thins as more hot core materials are coming down to join directly the continuum bed (Fig. 5.1). The other portions of crusts (those sticking to RPV wall) reach a steady profile affected by the ex-vessel cooling water (Fig. 5.2).
The temperature of the metallic layer arises in order to radiate the strong heat flux coming from the upper crust. At \( t = 2 \text{ h} \ 21 \text{ min} \), metallic layer temperature reaches the steel melting point temperature and at \( t = 2 \text{ h} \ 37 \text{ min} \), all metallic layer is melted (Fig. 6.2).

At \( t = 3 \text{ h} \), the aspect ratio \( (z_{lp} / R_{lp}) \) between the metallic thickness and metallic layer radius is about 0.16. The corresponding ratio metallic layer to wall heat flux oxidic pool to metallic layer heat flux (Figures 8) is 2.42 and is close to the value of 2.54 given by GAREC [8] for this aspect ratio assuming an homogeneous metallic phase in permanent state radiating upward.

Thus, as expected, the increasing superheat \( (T_{\text{metallic layer}} - T_{\text{steel melting point}}) \) leads to high convective heat fluxes to the vessel (Fig 7.2).
Besides, in the oxidic phase, 75% of power is directed upwards whereas 25% (Fig. 7.2) goes downwards which is found to be consistent with the COPO results [5].

As the heat flux to the wall increases, the vessel wall melts. The boundary nodes in contact with the metallic layer start to achieve the ferritic melting point. As a consequence, at t = 2 h 44 min, the RPV wall (lower node #5) undergoes a gradual melting (Fig. 10.2, Fig. 11) and mixes with the bulk debris in the lower plenum.

Therefore, the mass of the metallic layer increases (Fig. 13.2) as well as its level so that a part of the metallic layer faces the cylindrical part of the RPV.

Indeed, in our case where a large corium pool mass (Fig. 13.1 & 13.2) is relocated in the lower plenum, the largest heat flux is expected near the hemispherical part of the RPV i.e. near the lower part of the cylindrical part. In the MAAP4 nodalization (Fig. 2), the metallic layer faces the node #6 and at t = 3 h 07 min, this latter node begins to be eroded (Fig. 11) which leads to an other increase of the metallic layer mass.

**Fig. 8.**
- Fig. 8.1 Metallic layer aspect ratio (metallic layer thickness / metallic layer radius)
- Fig. 8.2 Ratio : metallic layer to wall heat flux / oxide pool to metallic layer heat flux

**Fig. 9.**
- Fig. 9.1 Heat flux from the upper crust to the metallic layer (1), heat flux from the metallic layer to the RPV wall (2)

**Fig. 10.**
- Temperatures of the lower head nodes (#5,#6) (see Fig. 2. for the lower head nodalization). The 5 curves given for each graph correspond to the division of each axial node into 5 radial meshes in the wall thickness.

**Fig. 11.**
- Nodal RPV thickness
However, as the metallic layer thickness increases (and consequently its aspect ratio), the heat flux from the metallic layer to the RPV wall decreases. The erosion slows down and an equilibrium is reached: no erosion occurs after $t = 4$ h 27 min.

At $t = 3$ h 56 min, the particulate debris bed starts to melt: the molten particulates relocate down to the continuum debris bed below; their oxidic and metallic constituents respectively join the metallic layer and the oxidic central pool. Thus, the oxidic debris mass keeps on increasing slowly whereas the core relocation to the bottom head has finished at $t = 3$ h 44 min (Fig. 13). After 6 h, the RPV wall thickness and the debris levels do not evolve any longer.

**Residual thickness**

The final thickness of node #5 is 2.04 cm (Fig. 11) and 9.8 for the node #6. This latter value seems too important compared to the metallic layer to wall heat flux ($\approx 1.5$ MW/m²) by a multiplicative factor of 4.

Indeed, a hand-made steady state calculation (with a steel thermal conductivity of 30 W/m/K and with a difference of temperatures through the wall of 1200 K) leads to a value of 2.4 cm for the residual thickness of the RPV wall.

However, it must be mentioned that only 40% of node #6 is covered by the metallic layer whereas 50% remains uncovered and radiates to the exposed heat sinks above (the remaining 10% of that node is covered by the oxidic pool).

As the nodal heat flux is area-averaged, the resulting heat flux to this node is less than the metallic layer to RPV one leading to an overestimated value of the RPV thickness. On the contrary, as the node #5 is mainly facing the metallic layer during the transient, the residual thickness of the RPV wall calculated by the code (2.04 cm) is consistent with the value calculated by hand.

**5.4 RPV failure assessment**

No RPV failure occurred in this transient with the MAAP4 code. Owing to the RCS depressurization strategy (the RCS and the containment have the same pressure), mechanical strength of the damaged vessel seems sufficient. In fact, only a small residual thickness of vessel at temperature below 800 K is necessary to hold the weight of debris. So, provided the outside of the vessel remains in nucleate boiling, the vessel could be saved.

**Code limitations**

It must be underlined that no dry out process is considered by the MAAP4 model outside the vessel wall. The heat transfer coefficient is calculated by the code with the Rohsenow's correlation (Nucleate Boiling) along the whole RPV wall instead of determining "local" heat transfer coefficients in front of each node depending on wall temperature. In fact, the critical heat flux will depend on position on the vessel: SULTAN [9] and ULPU 2000 [10] experiments show that critical heat flux can vary from 0.2-0.3 MW/m² at the base of the vessel to values close to 1.5 MW/m² when the surface approaches the vertical (cylindrical part). A transient spatial heat flux distribution should be used to assess the capability of the system to remove the debris heat flux.

**6. Conclusion**

The possibility to maintain the corium in vessel by external RPV flooding has been studied with MAAP4 code on a LBLLOCA scenario (surge line rupture) without safety injection. This scenario leads to the fastest accident
progression and to the highest decay heat to be removed from the corium into the lower head.

The reactor used was a high power PWR (4250 MWt) without lower head penetration. The influence of RPV thermal insulation was neglected.

The flooding of the reactor pit occurs early enough and the mass flowrate is sufficient for both phases (gravity flooding and flooding by the CHRS) to avoid any risk of dry out of the RPV wall by lack of water: RPV lower head is completely flooded at the time of corium relocation in the lower head (~1 h 30 min). The loops are flooded at 2 h 04 min; afterwards, the level does not decrease in the reactor pit. The residual thickness of the hemi-spherical part of the lower head (node #5) is correctly calculated by the code (around 2 cm). However, the MAAP4 value for the cylindrical part (node #6) is overestimated.

As MAAP4 considers only nucleate boiling heat transfer between the RPV wall and the external water in the reactor pit, the vessel wall is always well cooled and the code does not assess the risk of exceeding the Critical Heat Flux (CHF).

Nevertheless, in the ULPU experiments [10], the CHF near the hot spot in case of a molten pool in a lower head (i.e near the connection between the cylindrical part and the hemi-spherical part) is in order of magnitude of 1.5 - 1.6 MW/m², MAAP4 calculates a value of 1.5 MW/m² for the heat transfer between the molten corium metallic layer and the RPV wall in steady state. This value is consistent with values calculated by the CEA in the frame of the GAREC group assuming a 0D approach in the metallic layer [8].

Thus, in the thermal point of view, there is no sufficient margin if ULPU results are confirmed and if the temperature in the metallic phase can be considered as almost homogeneous for the bounding scenario studied.

As far as mechanical results are concerned, as the primary system is completely depressurized (PRCS = PCONTAINMENT = 3.5 bar), stresses in the RPV wall are only due to hydrostatic height so that MAAP4 shows that there is no rupture at short term (10 h) even with a residual thickness of 2 cm. This is normal as creep rupture time is very high for small mechanical load.

List of Symbols

- $g$: Gravitational acceleration, m/s²
- $\beta$: Volumetric coefficient of expansion, 1/K
- $H$: Length, m
- $q$: Volumetric heat generation rate, W/m³
- $v$: Kinematic viscosity, m²/s
- $\alpha$: Thermal diffusivity, m²/s
- $\lambda$: Thermal conductivity, W/m K
- $Nu_u$: Nusselt number for central molten pool upward heat flux
- $Nu_d$: Nusselt number for central molten pool downward heat flux
- $z_{lp}$: Corium elevation inside the lower plenum, m
- $R_{lp}$: Radius of the hemi-spherical part of the lower plenum, m
- $Pr$: Prandtl number
- $Gr$: Grashof number
- $Ra$: Rayleigh number (Ra=Pr.Gr)
- $\delta_{SS}$: Metallic layer thickness, m
- $T_{SS}$: Temperature of the metallic layer, K
- $h$: Melting point temperature of the metallic layer, K
- $\Delta T$: In eq. (8), $\Delta T = T_{wall} - T_{sat}$, K
- $\mu$: Viscosity of water, Pa.s
- $\rho_g$: Latent heat of water, J/kg
- $\rho_L$: In eq. (8), Gravita 1 kg/m³
- $\rho_s$: Density of water, kg/m³
- $\rho_c$: Density of steam, kg/m³
- $C_p$: Specific heat of water, J/kg/K
- $\sigma$: Surface tension, N/m

References

Coupled Thermal-Hydraulic Analyses of the Molten Pool and Pressure Vessel during a Severe Accident

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Abstract

Accidental scenarios in a PWR with core melt-down and flooding of the reactor pit have been considered in order to investigate the influence that the debris stratification could have on the cooling transient. A homogeneous and two stratified configurations of the debris (with different thickness of the metal layer) have been analysed by means of a finite element method. The model accounts for natural convection in the molten debris, heat transfer in the vessel wall, thermal radiation from the pool surface and for phase changes. The heat transfer between the external vessel wall and the cooling water has been considered by means of suitable correlations accounting for water boiling. In all the considered configurations the computed heat fluxes at the external vessel surface are below the CHF and the debris are retained within the vessel. Nevertheless the analyses show noticeable differences between the three configurations for the heat transfer from the molten pool to the cooling water. In particular for stratified configurations a peak of the lateral heat flux in correspondence of the metal layer has been predicted.

1 Introduction

In case of severe accident in a nuclear reactor with relocation of the molten corium in the Lower Head (LH) of the Reactor Pressure Vessel (RPV), the possibility of retention of the corium inside the vessel can drastically limit the consequences of the accident on the external environment. To provide in-vessel retention of the debris, external cooling of the vessel seems to be necessary almost when large amount of molten core is relocated in the LH. In this case the residual heat generated in the corium is evacuated through the vessel wall to the external cooling water. The effectiveness of the external cooling is strongly related with the boiling regime at the external vessel surface. Recent researches [2], [10], [11] indicate that in case of nucleate boiling the risk of a localized melt through of the vessel can be excluded but if the critical heat flux is reached somewhere then transition to film boiling is expected to occur with a drastic reduction of the cooling effectiveness.

The heat flux distribution from a hemispherical molten pool has been largely investigated in case of homogeneous pool. Some uncertainties exist in case of corium stratification with the formation of an upper metal layer and a lower oxide layer. In this case the upper, high conductive, metal layer can have a remarkable influence on the global heat transfer mechanism from the molten pool to the cooling water [1]. Even if there is not an experimental evidence that debris will form stratified layers this eventuality, in consideration of the density differences between the metal and the oxide phases, can not be completely excluded.

The work here presented has been focused to analyse the influence that an eventual corium stratification could have on the heat transfer from the debris. The debris cooling transient has been simulated for homogeneous and stratified configurations by means of coupled thermal hydraulic analyses of the molten corium and of the vessel. The heat transfer to the cooling water has been considered by means of correlations. The analyses are based on a finite element method and have been carried out using the CASTEM2000 FE code.

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2 Scenario

The analyses here presented refer to a PWR without penetration in the LH whose main dimensions are given in table 1.

<table>
<thead>
<tr>
<th>Inner Radius</th>
<th>Wall Thickness</th>
<th>Height</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.271</td>
<td>0.140</td>
<td>1.895</td>
</tr>
</tbody>
</table>

Table 1: RPV Lower Head geometrical characteristics [m]

A severe accident with core melting and relocation in the LH of part of the core mass is supposed to occur. The core melt down causes the partial melting of the lower internals (steel) that relocate in the LH. Flooding of the reactor pit is provided and assured during the investigated transient. The pressure within the containment is supposed to be 0.3 MPa and steam saturated equilibrium conditions to exist \( (T_{water} = 406 \, \text{K}) \). A constant decay power of 13 MW is present in the debris. The internal part of the vessel is dry and depressurized. The debris are a mixture of \( \text{UO}_2 \), \( \text{ZrO}_2 \) and steel.

To analyse the influence of an eventual debris stratification on the heat transfer three different configurations have been considered. In the first configuration the debris form a homogeneous pool and the decay power is uniformly distributed in the pool volume. In the second configuration the same amount of debris separates in two layers with the oxide phase relocated in the lower layer and the metal phase in the upper layer. In the third configuration the two phases separate in two layers but a lower amount of metal debris is assumed to be present in the pool. In both stratified configurations the decay power is uniformly distributed only in the oxide layer.

Different thermal properties and melting points have been considered for the metal, oxide and homogeneous pools. The debris relocation has not been simulated and the initial conditions of the simulated transients were: debris relocated in the LH in liquid state and at rest. The debris initial temperature for each configurations was \( T_0 = T_{melting} + 100\,\text{K} \).

<table>
<thead>
<tr>
<th></th>
<th>hmg</th>
<th>str04</th>
<th>str02</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cooling Water Temperature</td>
<td>K</td>
<td>406</td>
<td></td>
</tr>
<tr>
<td>Debris Residual Power</td>
<td>MW</td>
<td>13</td>
<td></td>
</tr>
<tr>
<td>Oxides Debris Total Mass</td>
<td>t</td>
<td>104</td>
<td></td>
</tr>
<tr>
<td>Metal Debris Total Mass</td>
<td>t</td>
<td>30</td>
<td>15</td>
</tr>
<tr>
<td>Pool total height</td>
<td>m</td>
<td>1.72</td>
<td>1.72</td>
</tr>
<tr>
<td>Oxide layer thickness</td>
<td>m</td>
<td>—</td>
<td>1.32</td>
</tr>
<tr>
<td>Metal layer thickness</td>
<td>m</td>
<td>—</td>
<td>0.40</td>
</tr>
</tbody>
</table>

Table 2: Synthesis of the scenarios

3 Modeling and boundary conditions

A coupled finite element method has been used to analyse the cooling transients. The pool and the LH have been discretized in \( \approx 5000 \) 4-nodes bilinear elements. The Navier-Stokes equations coupled with the energy conservation equation have been solved in the molten region to compute the velocity, pressure and temperature fields. An additional energy equation (transient diffusion) has been solved in the solid region to compute the temperature field. The energy equations on melt and solid sides have been coupled enforcing the continuity of the heat fluxes at the solid/liquid interface. A heat source term has been introduced in the energy equation (melt side) to account for the decay power. The model accounts for the phase changes and describes the formation and evolution of the corium crust as well as the melting (and re-solidification) of the
LH wall. It follows that the molten and solid region extensions vary during the transient. The turbulence effects have been modeled using a simplified model based on an eddy viscosity/diffusivity and mixing length theory ([5]). The problem has been analysed in axis-symmetric approximation.

3.1 Thermal boundary conditions

Thermal radiation has been considered for the upper pool surface with an emissivity coefficient of 0.7 in case of oxides and 0.3 in case of steel.

The boundary condition at the external surface of the LH accounts for the presence of the cooling water. The water has been considered in boiling condition and its temperature constant $T_{\text{water}} = 405K$ (the saturated equilibrium temperature at 0.3 MPa). Under nucleate boiling regime the heat flux has been computed according to the Rohsenow correlation [6], [7] on the basis of the physical properties of the water and of the temperature difference between the external surface of the vessel and the water

$$q = q_{\text{nuc}}(T_{\text{surface}} - T_{\text{water}})$$

If the heat flux $q$ reaches the critical value CHF, than film boiling is assumed to occur. The local CHF has been deduced by correlation based on experimental data [3], [4], [11]. When computing the cooling transients, at each time step, $q$ has been evaluated (for each node of the external surface) according to eq. 1 (on the basis of the local temperature difference between wall and water) and compared with the local CHF. If $q \leq \text{CHF}$ then $q$ has been used as boundary condition (heat flux at the external surface), otherwise the film boiling condition has been considered (but this condition has not been reached in the scenarios here presented).

3.2 Velocity boundary condition

The non-slip condition has been assumed as velocity boundary condition at the walls. In case of stratification the velocity component normal to the interface has been kept 0.

3.3 Physical properties

The corium physical properties have been computed weighting the physical properties of the components [8],[9] and [12]. In case of homogeneous pool the corium melting point has been estimated in 2540 K. For the stratified configurations melting points of 1700 K and 2850 have been respectively estimated for the metal and oxide layers. Important differences in thermal properties have been considered for the metal and oxide layers. A thermal conductivity $\lambda_{\text{oxide}} = 3$ and $\lambda_{\text{metal}} = 30 [\text{W/m/K}]$ has been assumed respectively for the oxide and the metal layers.

4 Results and comments

The cooling transient has been analysed for the three configurations till thermal steady conditions have been reached in the pool and in the vessel. The typical duration of the transient was $\approx 2$ hours (fig. 1). In homogeneous configuration (fig. 2) the molten pool is completely bounded by the corium crust and there is not direct contact between the molten corium and the vessel wall. The crust thickness increases along the hemispherical boundary and its maximum is localized at the pool bottom. The vessel temperature is everywhere below the steel melting point. This general behaviour is shown also in the early stage of the transient. The heat flux distribution at the external surface of the vessel (fig. 8) is always below the CHF and does not changes substantially during the transient. In the stratified configuration str04 (thick metal layer) (fig. 4) the oxide layer is bounded by the crust but its thickness is lower compared with the homogeneous configuration. The metal layer is separated from the oxide layer by the oxide crust and there is not direct contact between the two liquid layers. A large eddy is observed in the metal layer whose upper surface, also in presence of thermal radiation, does not solidify. During the early stage of the transient the vessel wall melts (partially) in correspondence of the metal layer (fig. 5) but re-solidification is observed. The heat flux
distribution at the external surface of the vessel (fig. 9) changes during the transient but is always below the CHF. Very high temperature gradients are localized in correspondence of the oxide crust with a thermal stratification in the lower part of the oxide layer (fig. 6). In the stratified configuration with thin metal layer (str02) a partial melting of the LH is observed in correspondence of the metal layer during the whole transient and is still present when the thermal steady conditions are reached (fig. 3). The behaviour of the oxide layer and the temperature distribution (fig. 7) are similar to the case str04. The heat flux distribution at the external vessel surface is very close to the case str04 in the lower part of the LH but an higher peak is observed in correspondence of the metal layer (fig. 10). Both stratified configurations, when compared with the homogeneous, show higher non uniformity in the heat flux distribution and higher peaks. The heat flux at the pool upper surface for the three configurations is shown in fig. 11. Higher heat fluxes are reached in case of homogeneous pool probably due to the higher thermal emissivity and temperature at the pool surface.

5 Conclusions

The thermal-hydraulic behaviour of the molten pool and vessel wall for three different configurations of the molten debris has been analysed. The debris configuration has remarkable effects on the global heat transfer and debris cooling transient. The heat flux distribution from the external vessel surface to the cooling water increases from the bottom toward the cylindrical part of the vessel. For homogeneous pool configuration the heat flux has a regular distribution and a flat max. over a region of ≈ 20 degrees. For stratified pool configurations the heat flux distribution shows two narrow peaks, a higher non uniformity compared with the homogeneous pool and higher peak levels. The metal layer thickness influences the heat distribution and higher flux peaks are reached in case of thin metal layer. A partial melting of the vessel wall has been observed in correspondence of the metal layer for both stratified configuration but not in case of homogeneous pool. The radiative cooling from the upper pool surface is more effective in case of homogeneous pool because of different radiative properties and temperatures of the upper layer.

References


Figure 1: Temperature history at the vessel wall for the case str02.
Figure 2: Homogeneous Configuration (hmg) at the end of the thermal transient. Velocity field and solid/melted region. (Isothermal lines A and B correspond to steel and pool melting points)

Figure 3: Stratified Configuration (str02) at the end of the transient. Velocity field and solid/melted region. (Isothermal lines A and B correspond to steel and oxide melting points)
Figure 4: Stratified Configuration (str04) at the end of the thermal transient. Velocity field and solid/melted region. (Isothermal lines A and B correspond to steel and oxide melting points)

Figure 5: Stratified Configuration (str04) after 810 s from the beginning of the transient. Velocity field and solid/melted region. (Isothermal lines A and B correspond to steel and oxide melting points)
Figure 6: Stratified Configuration (str04) at the end of the thermal transient. Temperature field.

Figure 7: Stratified Configuration (str02) at the end of the thermal transient. Temperature field.
Figure 8: Heat flux distribution around the LH for the homogeneous configuration (hmg) at different times.

Figure 9: Heat flux distribution around the LH for the stratified configuration (str04) at different times.
Figure 10: Heat flux distribution around the LH for the three different configurations (steady conditions)

Figure 11: Heat flux distribution at the pool top surface for the three different configurations (steady conditions)
STUDIES ON CORE MELT BEHAVIOUR IN A BWR PRESSURE VESSEL LOWER HEAD

I. Lindholm¹, K. Ikonen¹, K. Hedberg²

ABSTRACT

Core debris behaviour in the Nordic BWR lower head was investigated numerically using MELCOR and MAAP4 codes. Lower head failure due to penetration failure was studied with more detailed PASULA code taking thermal boundary conditions from MELCOR calculations. Creep rupture failure mode was examined with the two integral codes. Also, the possibility to prevent vessel failure by late reflooding was assessed in this study.

1 INTRODUCTION

The objective of this study is to investigate numerically how and when core melt migrates into the pressure vessel lower head, what is the temperature and the chemical composition of the melt and in what form (melt pool/ rubble bed) does the corium reside in the lower head. Furthermore, this investigation addressed the coolability of the debris in the lower head by reflooding and, finally, if the debris was not predicted to be coolable, what would be the failure mechanism of the lower head.

2 TECHNICAL APPROACH

The issue was investigated by performing plant calculations for typical ABB Atom type of BWR with MAAP4 and MELCOR 1.8.3 computer codes. The more detailed Lower Plenum Debris Bed model (i.e. BH package) was activated in MELCOR code. The thermal response of lower head penetrations were further assessed by performing separate thermal analyses with PASULA code using MELCOR results for debris bed composition and thermal history as boundary conditions [1].

PASULA is a common name for the group of heat conduction and structural analysis programs developed at VTT Energy. Heat conduction and convection analysis codes for two- and three-dimensional cases are based on the finite difference and control volume method. The codes are non-linear and take into account phase changes and latent heat. A new model for calculation of the effective heat conductivity in a porous or granular material was developed for the PASULA.

The base accident scenarios were station blackout with (low pressure case) or without (high pressure case) successful depressurization of the reactor coolant system. A number of sensitivity runs were performed with MELCOR and MAAP4, varying e.g. debris particle diameter and initial debris porosity. Reflooding and coolability of a dry debris bed was investigated by varying the timing of reflood and the water injection rate. The reactor pressure vessel failure was studied both in case of instrument tube failure and in case of lower head failure by creep rupture.

Table 1. Parameter variations for MELCOR and MAAP analyses.

<table>
<thead>
<tr>
<th>PARTICLE SIZE [µm]</th>
<th>POROSITY</th>
<th>START OF REFLOODING</th>
<th>REFLOOD CAPACITY [KG/S]</th>
</tr>
</thead>
<tbody>
<tr>
<td>MELCOR 2.0, 5.0, 10.0</td>
<td>0.3, 0.45</td>
<td>1 min-136 min after lower head dryout</td>
<td>45.0, 340.0</td>
</tr>
<tr>
<td>MAAP 0.9, 1.8, 3.6</td>
<td>0.3, 0.4</td>
<td>6-33 min after lower head dryout 34-44 min after lower head dryout</td>
<td>45.0 485.0</td>
</tr>
</tbody>
</table>

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² Vattenfall Energisystem AB, P.O.Box 528, S-16216 Stockholm, Sweden
3 RESULTS

In the MAAP4 analyses only creep rupture was considered since the MAAP4 model on instrument penetration ejection is not valid for the ABB penetrations. In general MELCOR/ BH model predicted considerably longer time to creep rupture than MAAP4 and the failure location in MAAP4 analyses was near the bottom center of the lower head, whereas MELCOR/BH model predicted the creep rupture to occur closer to the junction of the cylindrical and hemispherical parts of the lower head. The major reason to the differences is the different assumed layering of core material in the two codes, MAAP4 assumes oxides at the bottom and the BH model assumes oxides or mixture on the top.

3.1 Lower Head Penetration Failure

The core debris was only cooled, not quenched, during migration through the lower plenum water pool in the low pressure cases. The debris to coolant mass ratio was smaller in the high pressure cases and thus better quenching was achieved during fall down of material from the support plate. The BH package model is based on an assumption that debris is fragmented during relocation from the core region into the lower head. Due to this model all MELCOR and PASULA investigations address the thermal behaviour of structures in a granular debris bed.

The BH model in MELCOR is initiated after the lower head dryout. The particulate debris bed is arranged into three debris layers (two bottom most are depicted in Fig. 1). The heights of the debris layers vary with time.

If none of the lower head failure modes (instrument tube failure, wall ablation or creep rupture) was precluded in MELCOR/BH model, the lower head failure occurred by instrument tube failure in multiple locations in the middle of the debris bed. MELCOR predicted that the instrument tubes will fail due to melting 780 s after lower head dryout in the base low pressure case and 4600 s after lower head dryout in the base high pressure case.

The reduction of debris porosity in MELCOR accelerated the debris temperature escalation and the instrument tube failure occurred earlier in the high pressure cases. However, the reduction of debris bed porosity delayed the instrument tube failure in the low pressure cases. The main reason for this was that in case of lower porosity only part of the core material relocated into lower head prior to the lower head dryout and thus the debris bed was better cooled at the initiation of BH model. The initial conditions of the lower head model are sensitive to melt progression in the core region and especially to the controlling parameters of the support plate failure.

The particle size affected also the instrument tube failure time. With the larger particles (1 cm) the time gap from lower head dryout to instrument tube failure was doubled in comparison to the case with 5 mm particles.

Reflooding of lower head debris bed after lower head dryout and initiation of BH model could not prevent the failure of instrument tubes, on the contrary it made the instrument tubes fail earlier than in the dry bases cases. The reason for fast temperature rise in the debris bed was Zr oxidation. Augmentation of reflooding capacity from 45 kg/s to 340 kg/s enhanced the oxidation and caused an even earlier instrument tube failure. The oxidation was efficient independent of the selected particle size.
A thermal analysis of the instrument tubes was also carried out with PASULA code for low pressure case. Both instrument tube and control rod penetrations were investigated. The thermal history of the bulk debris was taken from MELCOR and given as input to PASULA. The composition and temperature dependent debris material properties and porosities calculated by MELCOR were translated to be functions of time for PASULA input. PASULA code calculated the thermal behaviour of the penetration structures and the heat transfer processes in the narrow "interfacial" porous debris layer surrounding a penetration. The heat transfer from the interface layer to the tube structure was defined by a calculated effective heat transfer coefficient taking into account radiation between the particles and steam conductivity in the interstitial space between the debris particles. A small contact area of the particles was also taken into account.

The PASULA calculations were performed for instrument tubes and control rod guide tubes residing at three different locations in the lower head (Fig. 1). According to PASULA calculations, the lower head failure would occur most likely due to instrument tube weld failure at the outer ring (2,4) of the lower head about 4000 s after lower head dryout. The supporting welds of the instrument tubes in the centre of the lower head (2,1) would fail due melting in about 5000 s at an elevation of about 1 m from the bottom wall. However, it is likely, that the debris pouring into the flow channel freezes and blocks the channel before discharging out of the vessel. The control rod nozzles would also lose strength at about 4000 s after lower head dryout near the periphery of the lower head. The control rod guide tubes are supported from outside the vessel by a common tie plate, and the simultaneous failure of a few control rod tubes would not lead to tube ejections and debris discharge out of the vessel.

![Fig. 1](image)

**Fig. 1.** Lower head nodalisation in MELCOR and three locations for analysed penetrations.

### 3.2 Lower Head Creep Rupture Failure

Both MELCOR and MAAP4 codes were applied to investigate the creep rupture failure of the lower head. The instrument tube failure was precluded by setting of an input parameter. The key results of the creep rupture failure studies are summarized in Table 2.
Table 2. Key results of MELCOR and MAAP4 calculations with only creep rupture failure mode enabled.

<table>
<thead>
<tr>
<th>Case</th>
<th>Low pressure case</th>
<th>Low pressure case</th>
<th>High pressure case</th>
<th>High pressure case</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>No reflooding</td>
<td>Reflooding</td>
<td>No reflooding</td>
<td>Reflooding</td>
</tr>
<tr>
<td></td>
<td>MELCOR</td>
<td>MAAP4</td>
<td>MELCOR</td>
<td>MAAP4</td>
</tr>
<tr>
<td>Particle size (mm)</td>
<td>5</td>
<td>3.6</td>
<td>5</td>
<td>3.6</td>
</tr>
<tr>
<td>Porosity</td>
<td>0.45</td>
<td>0.4</td>
<td>0.45</td>
<td>0.4</td>
</tr>
<tr>
<td>Support plate failure (min)</td>
<td>326</td>
<td>84</td>
<td>326</td>
<td>84</td>
</tr>
<tr>
<td>Lower head dryout (min)</td>
<td>348</td>
<td>158</td>
<td>348</td>
<td>133</td>
</tr>
<tr>
<td>Start of reflooding (min)</td>
<td>358</td>
<td>45 kg/s</td>
<td>359</td>
<td>45 km/s</td>
</tr>
<tr>
<td>RPV creep rupture (min)</td>
<td>676</td>
<td>274</td>
<td>585</td>
<td>-</td>
</tr>
<tr>
<td>Time between LH dryout and creep rupture</td>
<td>5.5 h</td>
<td>1.9 h</td>
<td>4.0 h</td>
<td>-</td>
</tr>
<tr>
<td>Er oxidation fraction</td>
<td>16.6 %</td>
<td>16.4 %</td>
<td>15.6 %</td>
<td>15.6 %</td>
</tr>
</tbody>
</table>

In the MELCOR calculation the debris bed resumed the heat up after the lower head dryout. Steel components in the debris bed melted and moved downwards reducing porosity of the bottom layer. Temperatures of the lower head wall and the baffle plate structures increased leading to melting and relocation of the shroud structures. In the base case the reactor pressure vessel was predicted to fail 5.5 h after lower head dryout due to creep rupture in wall node 16 at the average nodal wall temperature of 1696 K. The debris bed temperatures and porosities just before local creep rupture are shown in Figure 2.

For MAAP4 the first relocated debris batch from the core region was assumed to fragment. The following material batches did not experience total fragmentation and the melt formed crusts on the equipment and on the RPV wall instead. About 50 minutes later steel structures melted and formed a metal layer on top of the upper crust of the ceramic melt pool. The particulate debris bed resides on top of the metal layer. At 1.9 hours after LH dryout the RPV wall failed by creep rupture in the lowermost node. The temperatures in the lower plenum at the time of vessel failure are shown in Figure 3.

For the high pressure cases the creep rupture occurred earlier and the MELCOR and MAAP4 predictions agreed better in general. The debris temperature was lower and the porosities were higher at the time of creep rupture. Figs 4 and 5 illustrate the debris temperatures predicted by MELCOR and MAAP4, respectively, prior to the creep rupture.

According to the MELCOR model, the reflooding (with HPCI system) of lower head does not prevent creep rupture, if a lower head dryout has once occurred. The maximum debris temperature increased inspite of coolant injection in low pressure case (Fig.6).

In the case of late reflooding the porosities of MELCOR layers 1 and 2 were almost zero and water could not sufficiently penetrate the debris bed to cool the corium. In the case with early lower head reflooding the porosities of the debris bed were high enough for water to penetrate the porous bed, but the evaporation driven oxidation in the debris bed (800 kg of hydrogen was produced in 18 minutes) released more heat than was transferred to coolant. Again material melted and porosities in the layers 1 and 2 were reduced to zero and entrainment of water was stopped. The only case, where MELCOR predicted coolability was in high pressure case if the water injection was initiated 1 minute after the dryout, when the debris temperature was well below the starting temperature of the rapid oxidation (Fig. 7).
**Fig. 2.** Lower head temperatures at creep rupture. Wall temperature is at the inner surface. MELCOR calculation. Olkiluoto low pressure case.

**Fig. 3.** Lower head temperature at creep rupture. MAAP4 calculation. Forsmark 3 low pressure case.

**Fig. 4.** Lower plenum temperatures in Olkiluoto high pressure case at creep rupture. MELCOR calculation.

**Fig. 5.** Lower plenum temperatures in P3 high pressure case at creep rupture. MAAP4 calculation.
Fig. 6. Maximum debris temperature in the lower head. MELCOR calculation. Okiluoto low pressure case.

Fig. 7. Maximum debris temperature in the lower head. MELCOR calculation. Okiluoto high pressure case.

In the respective MAAP4 calculations for Forsmark 3 the reflooding mass flow rate was higher, 485 kg/s, compared to MELCOR calculations (45 kg/s). The debris configuration was easily coolable according to MAAP4, because the reflooding water could cool both the lower debris and the RPV wall due to the assumed gap between the lower debris crust and the RPV wall.

4 DISCUSSION OF RESULTS

The experimental data and also the performed code calculations suggest that the debris is, to significant extent, fragmented and cooled during the fall down from the support plate to the lower head. Large differences between the code models exist in the formation of the lower head debris bed. The order in which the different materials relocate from the support plate determines the composition of the debris bed.

The instrument tube failure seems to be the first option for the failure of an ABB Atom reactor vessel bottom head. The time for penetration failure after lower head dryout is around 1 hour in case of particulate debris. If a large mass of melt pours rapidly into the lower head with enough super heat to make a smooth contact with the penetration tube, the expected failure time would be reduced by factor of ten [2].

The creep rupture failure times of the lower head for the MAAP4 and MELCOR calculations are very different. The lower head debris bed has different composition and state of material in the two codes. Major differences were encountered in the models of debris coolability. MAAP4 model is based on an assumption that in presence of water a narrow gap forms between the lower crust and the RPV wall. Coolant is assumed to penetrate into the gap and provide an efficient cooling mechanism for the debris bed. MELCOR does not take credit of a gap cooling model. Another important model difference is that MAAP does not account for any oxidation in the lower head debris bed, while in MELCOR calculations the oxidation of particulate debris during reflooding seemed to be the major driving mechanism for further heatup.
Rempe et al. estimate in their comprehensive late phase melt progression study [3] that the lower head pool boiloff time would be ~1-6 hours in BWRs. MELCOR calculations for Olkiluoto resulted in boiloff times of 21 - 47 minutes and MAAP4 calculations for Forsmark 3 predicted boiloff times of 36 min - 1.7 h. They also conclude that peak vessel temperatures in uniform metallic or ceramic debris beds occur at the bottom, near the debris/vessel wall surface. In stratified debris beds the locations of peak vessel temperatures vary during the transient and tend to occur higher at the debris/wall interface, near the point where the skirt attaches to the vessel. This result agrees with the MAAP4 and MELCOR predictions.

The BWR high pressure scenario analysed in [3] resulted in creep rupture after 3.7 hours, when wall internal temperature was 1210 K. The result of MELCOR/BH calculation of Olkiluoto high pressure scenario (without tube failure) is in rather good agreement with it (creep rupture at 3.3 h, internal wall temperature 1060 K). In the respective MAAP4 calculation the creep rupture occurred significantly earlier, in 54 minutes at the wall temperature ~ 1300 K.

The key uncertainties identified in the presented analyses are:

1. Initial quenching fraction of debris, when slumping into lower head water pool.

2. Corium flow from the reactor pressure vessel through a failed instrument tube. Formation of a blocking crust is highly dependent on the flow characteristics and the initial hole size.

3. Coolability of particulate debris bed in the lower head by reflooding. There is scarce information about the oxidation in a rubble bed and its effects on thermal response of lower head. Also the gap formation between the debris bed and the lower head wall and its cooling capacity still needs further demonstration.

5 SUMMARY AND CONCLUSIONS

Debris bed behaviour and thermal response of structures in the reactor vessel lower head was studied in case of Olkiluoto and Forsmark BWRs. Both low and high pressure scenarios were analysed with sensitivity studies addressing the effects of debris bed porosity, debris particle size and reflooding of dry debris bed. Lower head failure mechanisms and timing were examined. The plant analyses were performed with MELCOR/BH and MAAP4 computer codes. A detailed thermal analysis was performed for instrument tube penetrations with PASULA code.

In low pressure cases the debris was partially quenched during the initial down fall from the core region. If the lower head penetration model was active, the lower head failed by instrument tube melting in multiple radial locations above 60 cm from the bottom of the vessel 13-50 minutes after lower head dryout. PASULA calculations taking thermal boundary conditions from MELCOR suggest that lower head would fail due to instrument penetration weld failure about one hour after the lower head dryout. If the instrument tube failure was precluded, the lower head failed due to creep rupture. MAAP4 and MELCOR predictions for the lower head creep rupture differed substantially. MELCOR predicted that the rupture occurs 5.5 hours and MAAP4 1.9 h after lower head dryout.

In high pressure cases the debris was initially quenched in the lower head water pool. According to MELCOR the lower head failed due to instrument tube melting at 55 min -1h 17 min after the lower head dryout. If penetration failure was precluded the lower head failed due to creep
rupture, according to MELCOR about 3.3 hours and according to MAAP4 about 1 h after the lower head dryout.

Reflooding of dry lower head debris bed prevented the (creep rupture) failure of the lower head in MELCOR calculations only if reflooding was started immediately (1 min) after lower head dryout. MAAP4 predicted a larger time marginal for the start of reflooding. A coolable state was reached if reflooding was initiated at least 20 min before calculated creep rupture time.

The studies suggest that the instrument tube welds fail first in the Nordic BWRs. Even if the failure of an instrument tube occurs, large uncertainties exist in the debris discharge rate to the containment, which is dependent on the debris composition and melt fraction. If only creep rupture failure is considered, the calculated lower head creep rupture times in high pressure cases are in agreement with the work performed earlier by Rempe et al. However, large differences exist in the creep rupture times under low pressure conditions.

ACKNOWLEDGEMENTS

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REFERENCES


Analysis of Reactor Vessel Lower Head Penetration Tube Failure

M.M. Stempniewicz, KEMA/NUC, The Netherlands
17th of February, 1998

Abstract

This paper presents results of two studies, performed to investigate the behavior of the reactor vessel penetration tubes in case of relocation of molten material into the tubes. The first study is on the CORVIS drain line experiment 03/1. Results of pre-test calculations are presented, and compared to the later obtained experimental data. The timing of the drain line melting and the velocity of the debris flowing inside the drain line were predicted correctly, but the penetration depth was clearly underestimated. If the calculations are done using different correlation for the melt-to-wall convective heat transfer, the results are closer to the experiment. It cannot however be concluded that the alternative correlation is more appropriate until other uncertainties are clarified.

The second study presents calculations performed for GKN Dodewaard CRD, instrument tubes and drain line. Calculations were performed to estimate whether the tubes have a chance to withstand the first attack of the melt and thus postpone vessel failure until the water in the lower plenum evaporates. Calculations were performed assuming that the melt can move into the tubes without any resistance, e.g. presence of water in the tubes was not taken into account. The results indicate that the critical penetration of the GKN vessel, which is most likely to fail, is the drain line. Results also indicate that external flooding should prevent early tube failure, at least in case of low vessel pressure.

1. Introduction

This paper presents results of numerical thermal and structural analyses performed to investigate the behavior of penetration tubes, in case of relocation of molten material into the tubes.

Two analyses are presented. In the first part (section 2) the pre-test calculation of the CORVIS experiment 03/1 are presented. The pre-test calculations were performed using the ANSYS code, and are documented in [Stevens, 1994]. The experiment was performed at PSI in December 1994. This paper presents the results of the analysis made by Stevens, as well as the comparison with the results of the experiment. The main purpose of this part of the paper is to compare the pre-test calculations with the experimental data and to analyze the differences between them.

The second part (section 3) presents results of the analysis performed to investigate the behavior of the penetration tubes for the Dutch NPP with a BWR reactor: GKN Dodewaard [Stem, 1994]. The background of this analysis is the differences observed in predictions of fully integrated computer codes: MELCOR and MAAP. In MELCOR penetration tubes are molten and the vessel is breached almost immediately when the first substantial amount of debris relocates to lower plenum. In MAAP the water present in the lower plenum typically cools down the debris before it can destroy the penetration tubes.

The main purpose of this analysis was to estimate whether the penetration tubes can withstand the first attack of the melt. If this happens then a crust should be formed in the lower plenum and the vessel failure would be postponed at least until the water from the lower plenum evaporates. Another purpose was to identify a critical penetration and estimate the influence of ex-vessel flooding on the tube behavior.

2. Analysis of the CORVIS Drain Line Experiment

2.1. Introduction

CORVIS experiments on drain line failure have been performed at PSI, Switzerland [Cripps, 1995]. Within the scope of the Dutch research program PINK ("Programma voor Instandhouding van Nucleaire Kennis", or "Program for Intensifying Nuclear Competence") pre-test calculations of the experiment 03/1 have been performed at KEMA [Stevens, 1994]. This section presents the results obtained by Stevens and a short comparison with the experimental results obtained later.

2.2. Approach

The pre-test simulation of the CORVIS drain line experiment, No. 03/1 was performed using the finite element program ANSYS 5.0 [ANSYS]. The following two periods are distinguished for the analysis:

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Date: 1998-02-24
(a) flow of the molten debris inside the tube,
(b) conduction within the stagnant debris.

To perform ANSYS calculations for this case one needs to know the time of the flowdown period, freezing distance, and the heat transfer coefficient between the melt and the drain line wall. Those values were estimated based on a simplified reasoning, and calculated with a general purpose program MATHCAD. With those values ANSYS calculations are performed for two different periods. In the initial (debris flowdown) period convective link is modeled between the flowing debris and the drain line wall (use is made of the estimated heat transfer coefficient). In the second period debris is assumed to be stagnant. 2-D convection is calculated for all nodes.

2.3. Analysis

2.3.1. Model

Figure 2.1 shows a part of the lower head, with the drain nozzle and drain line. Thermal response of the critical part of the drain line is analyzed using the finite element program ANSYS 5.0 [ANSYS]. The nodalization is shown in figure 2.2. The structure is modeled as axial symmetric. The model includes part of vessel head, drain nozzle and drain pipe. At the cut-off boundaries insulation boundary conditions were set. The space occupied by debris is also modeled. In case of the flowdown period the debris nodes are held at constant temperature (see section 2.3.6 below) and transfer heat to the wall using the 2-D convection links.

Those of the inner steel nodes, which temperature is calculated to exceed the melting point at this period, are "de-activated" (heat capacity changed to zero and conductivity changed to infinity) to simulate the removal of molten elements of the inside wall of the drain pipe.

When the flowdown period is finished the temperature of the debris nodes is allowed to change. In this part of the analysis heat is transferred by conduction using 2-D conduction model.

2.3.2. Geometry Data

Geometry of the analyzed CORVIS drain nozzle is shown in figure 2.1. The figure was adapted from [Cripps, 1995]. All dimensions shown in figure 2.1 are expressed in millimeters. Above the nozzle there is vessel plate (not shown in the figure), 100 mm thick. Drain tube is welded to the bottom of the nozzle.

2.3.3. Material Data

The material of the vessel head, drain nozzle, and the drain pipe are assumed to have ferrite structure. The properties for such material are taken from [Richter, 1973]. The density, thermal conductivity, and specific heats are shown in figures 2.3 - 2.5. The heat of fusion, equal to of $3.0 \times 10^3 J/kg$, was defined through the specific heat data (figure 2.5). The melting temperature range was assumed to be: 1753 - 1793 K.

The properties of the melt (composed of iron and Al$_2$O$_3$ - see [Cripps, 1995]) are expected to be close to the properties of the ferritic material. Therefore the same properties are used for the molten debris.
the vessel is assumed to be equal to 305 K. The initial temperature of the debris is assumed to be 2673 K (2400 °C). That means the initial superheat is about 900 K.

2.3.5. Boundary Conditions

It is assumed that at the outer surface of the drain line and the vessel head heat is lost by convection and thermal radiation. The radiative heat transfer is assumed to be equal to:

\[ q_{rad} = \varepsilon \sigma (T_s^4 - T_b^4) \]

where:
- \( \varepsilon \) - surface emissivity, assumed equal to 0.7,
- \( \sigma \) - Stefan-Boltzmann const. (5.67 \times 10^{-8} \text{ W/m}^2\text{K}),
- \( T_s \) - surface temperature, K,
- \( T_b \) - temperature of the surrounding space (equal to the bulk gas temperature), K.

For an analysis with the use of the ANSYS code one needs to have an overall heat transfer coefficient, defined as the overall heat flux, divided by the temperature difference. The overall heat transfer coefficient consists of the convective and the radiative coefficients:

\[ h = h_{conv} + h_{rad} = \frac{\varepsilon \sigma (T_s^4 - T_b^4)}{T_s - T_b} \]

The value of convective heat transfer coefficient was set to 20 W/m²K. The overall heat transfer coefficient was tabulated as a function of surface temperature.

2.3.6. Results

The results are discussed in three parts. The first part describes the results of the analytical/MATHCAD model to determine the key parameters needed to perform ANSYS calculations: flow velocity, heat transfer coefficient, and freezing time and distance. The second part describes the part of the ANSYS calculations for the melt flowdown period. The last part describes the ANSYS results for the conduction period, when the debris front is already frozen and the debris inside the tube is motionless.

**Estimation of the key debris flow parameters**

The key parameters needed to perform ANSYS calculations are the melt flow velocity, heat transfer coefficient, and flowing time. Those values are estimated as follows.

Velocity of the debris inside the drain line is calculated using the Bernoulli law, written for the points: (0) - at the surface of the debris in vessel, and (1) - at the debris front.
\[ p_0 + \rho g h_0 + \frac{\rho v_0^2}{2} = \]
\[ = p_i + \rho g h_1 + \frac{\rho v_1^2}{2} + \frac{\Delta p}{\xi} + \int \rho \frac{\partial v}{\partial t} \, dx + \Delta p \]

with:
- \( p \) - static pressure, Pa
- \( \rho \) - density, kg/m\(^3\)
- \( g \) - gravity constant, m/s\(^2\)
- \( h \) - column height of debris (subscripts refer to points 0 and 1), m
- \( v \) - velocity of debris (subscripts refer to points 0 and 1), m/s
- \( \Delta p \) - pressure loss between points 0 and 1, Pa
- \( \xi \) - axial coordinate variable, m
- \( t \) - time, s

It is assumed that the pressure loss can be calculated from:
\[ \Delta p = \xi \frac{\rho v^2}{2} \]

where \( \xi \) is the loss coefficient. At the location of the debris front the column height of debris is zero. It is further assumed that at the debris surface the velocity is zero, and that the static pressure at the location of the front is equal to the static pressure at the debris surface. With those assumptions the above equation reduces to:
\[ \rho g h_d(t) = \frac{\rho v_i(t)_0^2}{2} (1 + \xi(t)) + \int_0^t \frac{\rho}{\xi} \frac{\partial v(x,t)}{\partial t} \, dt \]
in which it is explicitly noted which parameters are allowed to vary in time or space.

Debris front position is determined by the integral of front velocity:
\[ x(t) = x(t=0) + \int_0^t v(t') \, dt' \]

The resistance coefficient at a given position of the debris front is calculated from:
\[ \xi(x(t)) = \frac{\lambda}{D} + \sum_i K_i \]

where:
- \( \lambda \) - friction factor, \( \cdot \), assumed equal 0.02
- \( D \) - inside diameter of the drain line, m
- \( K_i \) - additional resistance due to bend, \( \cdot \)

The above equations are solved using MATHCAD to give the front velocity as a function of time. The calculated values of the front velocity, \( v \), and the penetration depth, \( x(t) \), are shown in figure 2.6. The mean front velocity of the debris is calculated at 3 m/s.

The next parameter: heat transfer coefficient, is calculated from the following empirical correlations, valid for turbulent forced convection in pipes [VDI, 1988]:
\[ \frac{h(t)}{D} = \frac{\sqrt{8} \left( \frac{Re}{1000} \right) Pr}{(1 + 12.7 \sqrt{8}) \left( \frac{Pr}{20} - 1 \right)} \]
where Re, Pr, are Reynolds and Prandtl number respectively, and \( \xi \) is defined by:
\[ \xi = (1.82 \log_{10}(Re) - 1.64)^2 \]

The calculated values are shown in figure 2.7. The mean value is 54,000 W/m\(^2\)/K.

The correlation shown above, has been used in the original calculations [Stevens, 1994], although it is not valid for molten metals, and should not be applied here. Appropriate correlation for liquid metal flow in a circular tube is [Dwyer, 1963]:
\[ Nu = 7 + 0.025 Pe^{0.8} \]

In the present case Peclet number is equal to: \( Pe = D \cdot v \cdot \rho \cdot c_p / k = 0.0429 \cdot 7200 \cdot 700 / 32 = 20,300 \). Nusselt number is: \( Nu = 7 + 0.025 \cdot 20300^{0.8} = 76.8 \). The heat transfer coefficient: \( h = Nu \cdot k / D = 76.8 \cdot 32 / 0.0429 = 57,300 \) W/m\(^2\)/K, is luckily enough very close to the one which was obtained originally by Stevens.
The next parameter, time of the debris downflow in the drain tube, is estimated from an energy balance, written for the front of the debris. The melt at the front is constantly flowing along cold (not yet heated) material. It is assumed that the melt at the front is transferring heat only by convection to the cold walls of the drain pipe. The heat that is conducted from the warmer debris, behind the front is neglected in the calculations. The energy balance for a debris front layer of the thickness $dx$ is:

$$V \rho c_p \frac{dT(t)}{dt} = A h (T_o - T(t))$$

where:
- $V$ - melt volume at the front, ($\pi D^2dx$/4), m$^3$,
- $A$ - heat transfer area, ($\pi Ddx$), m$^2$,
- $T$ - temperature of the debris front, K,
- $T_o$ - cold wall temperature, K,
- $D$ - inside diameter of the drain line, m.

The above differential equation has been integrated using the MATHCAD program. Note that $\rho$ and $c_p$ are time dependent, since they depend on current temperature (specifically rapid is the $c_p$ change in the phase change region - figure 2.5). The resulting front temperature as a function of time is shown in figure 2.8. The melt front begins to solidify after about 0.46 s, and is completely frozen at about 0.76 s. Making use of the calculated already front velocity, the temperature of the front is plotted as a function of the penetration depth in figure 2.9. As shown there the melt begins to solidify when it is at about 1.4 m and is frozen completely at about 2.3 m below the vessel head.

With the values of the flowdown velocity, heat transfer coefficient and flowdown time period, calculated as described above, the analysis is made using the ANSYS code. The values applied in ANSYS are the average over the calculated period. That means the following values were applied in ANSYS calculations:
- velocity of molten debris: 3.0 m/s,
- heat transfer coefficient: 5.4\times10^4 W/m$^2$/K,
- debris flow time period: 0.76 s.

The ANSYS results are described below.

**Debris flowdown period (0.0 - 0.76 s)**

In this period convection from the flowing melt to the inside wall surface takes place. The value of the bulk temperature of the melt at certain axial location is determined by linear interpolation between the temperature at the tube entrance and the temperature of the debris front temperature. With this approach the temperature of the melt at certain location stays constant during the whole flowing time.

At a certain time point, $t$ (in the time range from 0 to 0.76 s), the melt front is somewhere in the drain line or the nozzle. According to the position of the melt front appropriate convection links in ANSYS are activated. This means that if the debris front is above a given location in the drain line or nozzle then the convection links at that location are not activated.

If the temperature of a certain node of the nozzle or drain pipe is at a certain time calculated to be higher than the melting temperature then this node is "de-activated" (it is assumed to be flushed away by the melt). This is done by setting the material heat capacity of that node to a very small value and the thermal conductivity to a very large value.

Calculated temperature behavior is shown in figure 2.10. This figure shows the drain line wall inside and outside temperatures versus time for the critical axial location, which turned out to be at about 1 centimeter below the drain nozzle (axial location, $x$, equal to about 0.21). The period discussed here (0 - 0.76 s) is seen at the left part of the figure. The temperature at the outside of the tube rises by about few hundred K in that period. The inside temperature rises very quickly to the melting temperature and then stays close to that value as this node is "washed away". For the rest of that period the temperature of this node oscillates somewhat above the melting point, as the next, deeper nodes, are being "washed away". At the end of that period the temperature of this node is set to the debris temperature at the current location, which for $x=0.21$ is about 2250 °C (see figure 2.9).
Conduction period (0.76 - 20.0 s)

At \( t = 0.76 \) s the debris is assumed to stop because the front is totally frozen. The nodes representing the space inside the drain line, as well as those nodes representing the drain line wall which have been "washed away" during the previous period, are now activated. That means the material properties of the nodes is set to the properties of the debris and the temperatures are set to the same values as used for the bulk temperatures during convection. The prescribed temperatures of the melt are now released, so that now the melt temperature decreases as the heat is conducted to the walls of the drain pipe. The results for critical axial location are shown in figure 2.10. The debris is cooled down from above 2200°C to about 1600°C in a few seconds. The outer part of the tube reaches the melting temperature at about 9 s.

2.4. Summary

The obtained results indicated that melt through of the CORVIS drain pipe is expected after about 9 seconds from the start of the penetration of the melt into the drain line. Failure of the tube is expected to occur a little earlier. At this time the front of the debris has already been frozen and the debris flow was stopped. The time of melt flow through the drain pipe is about 0.8 s. The penetration depth is about 2.3 m, measured from the elevation of the inside surface of the vessel bottom plate.

2.5. Comparison with Experiment

The CORVIS experiment 03/1 has been performed at PSI on December 15, 1994. The results were as follows:

The drain line melted through at 6.2 s after the arrival of melt in the experimental vessel ([Cripps, 1995], section 6). The melt was not frozen in the drain line. The melt flowed through the full length of the drain line for about 5 s. After the rupture the drain tube was tilted aside and the remaining melt flowed directly to the catcher through the central hole in the test plate.

The fact of the rupture and the timing were predicted correctly in the pre-test analysis made by Stevens. The velocity of the debris inside the tube was also quite well predicted: 3.0 m/s in the analysis and about 3.4 m/s (length: 4.1 m, divided by the tube filling time 1.2 s [Cripps, 1995], p. 6) in the experiment. The most important difference was the fact that Stevens predicted that melt front will freeze in drain tube at 2.3 m, while in the experiment it didn't freeze on the whole length (4.1 m - see [Cripps, 1995], figure 4).

The penetration distance may be calculated alternatively using a method presented in [Rempe, 1993]. For circular tubes, without internal coolant, the formula is:

\[
\frac{X_p}{D} = \frac{0.25 \cdot Pe^{0.6}}{Nu} \frac{T_d - T_m + h_f/c_p}{T_d - T_o}
\]

where:
\( X_p \) - penetration distance, m,
\( T_d \) - debris initial temperature, K,
\( T_m \) - debris melting temperature, K,
\( T_o \) - initial tube temperature, K,
\( h_f \) - heat of fusion of the debris, J/kg,
\( c_p \) - specific heat of the debris, J/kg/K.

(It may be shown that the Stevens' method leads to the same formula, if Pe and Nu are constant along the tube.) The correlation recommended by Rempe for the Nusselt number in case of circular tubes is:

\[
Nu = 0.625 Pe^{0.6}
\]

which leads to ([Rempe, 1993], equation 4-29):

\[
\frac{X_p}{D} = 0.4 \cdot Pe^{0.6} \frac{T_d - T_m + h_f/c_p}{T_d - T_o}
\]

If that correlation is used, together with the experimental velocity of 3.4 m/s, then the obtained values are:

\[
Pe = \frac{D \cdot \rho \cdot c_p}{k} = \frac{0.0429 \cdot 3.4 \cdot 7200 \cdot 700}{32} = 23,000
\]

\[
Nu = 0.625 Pe^{0.6} = 0.625 \cdot 23,000^{0.6} = 34.7
\]

\[
X_p = D \cdot \frac{0.25 \cdot Pe^{0.6}}{Nu} \frac{T_d - T_m + h_f/c_p}{T_d - T_o} = 0.0429 \cdot 0.25 \cdot 23,000 \frac{T_d - T_m + h_f/c_p}{T_d - T_o} = \frac{2673 - 1773}{2673 - 305} = 0.4
\]
Analysis of Reactor Vessel Lower Head Penetration Tube Failure

The obtained penetration distance, \( X_p \), is very close to the total length of the drain line, indicating that there should be no freezing of the debris inside the drain line. This fact indicates that the method recommended by [Rempe, 1993] gives better agreement with the experimental results than the method applied by Stevens. It is interesting to notice one other thing. The method recommended by Rempe is based on the Nusselt number correlation:

\[
Nu = 0.625 \ Pe^{0.4}
\]

As it is pointed out by the author, this correlation is a "conservative" one (see [Rempe, 1993], page 4-32), presumably meaning that the obtained Nusselt numbers which this correlation gives are too low. If one uses the presumably "best estimate" correlation, which according to [Dwyer, 1963] is:

\[
Nu = 7 + 0.025 \ Pe^{0.4}
\]

then the value of the Nusselt number (based on \( Pe = 23,000 \) will be equal to: \( Nu = 84.1 \), and the penetration depth equal to: \( X_p = 1.65 \), even smaller than predicted originally by Stevens (2.3 m).

In another CORVIS experiment (03/2) it was concluded that if the tube is empty the melt will penetrate practically infinite distance [Hirschmann, 1997]. Those results indicate that the method based on the bulk freezing concept is not appropriate and the conduction layer freezing may be preferable. The conduction freezing leads to the formula ([Rempe, 1993], equation 4-32):

\[
X_p = \frac{D \ Pe}{16 \ \lambda_e}
\]

where \( \lambda_e \) is the solidification constant, which varies from 0.2 to 0.75. Assuming the larger value of \( \lambda_e \), the penetration depth for the present case is equal to: \( X_p = 0.0429 \times 23000/16 \times 0.75^2 = 110 \) m. This result explains better the melt behavior observed in the CORVIS experiments.

The fact that the conduction layer freezing may be preferable from the bulk freezing was also concluded from the post-test examination of the drain tube ([Cripps, 1995], section 6). However, the measured thickness of the frozen layer was increasing with the distance, which is contradictory to the conduction layer freezing model. This discrepancy may be caused by the fact that the model is based on the assumption that the melt is at or near its melting point, and in the 03/1 experiment the melt was initially largely superheated.

2.6. Conclusions from CORVIS Analysis

In the pre-test calculations of CORVIS test 03/1 the fact of the rupture and the timing were predicted correctly. Also the debris velocity in the drain tube was correctly predicted. The penetration depth, calculated based on the bulk freezing concept, was largely underestimated. This indicates that the conduction layer freezing model may be preferable. The same was concluded from the post-test examination of the drain tube.

3. Analysis of GKN Penetration Tube Failure

3.1. Background

As a part of PSA level II made for the GKN Dodewaard Nuclear Power Plant a number of severe accident scenarios were analyzed using fully integrated computer codes: MELCOR [Summers, 1994], and MAAP [FAI, 1993]. Typical scenarios lead to relocation of large mass of corium to the RPV lower plenum, at the time when substantial amount of water is present in the lower plenum. In such cases vessel failure times predicted by MAAP and MELCOR were very different. MAAP predicts quenching of the molten corium in the lower plenum. Heat-up and failure of the vessel structures does not occur until all water from the lower plenum is evaporated. On the other hand, MELCOR typically calculates almost immediate vessel failure, once the debris relocates to the lower plenum. It is a result of the modelling approach taken in the code (see [Summers, 1994], COR Package, section 4). This seems to be too pessimistic, specifically in view of the TMI experience [NSAC, 1980].

The purpose of the analysis is to estimate the possibility of an early (before lower plenum dryout) vessel failure due to heat up and rupture of the penetration tubes, against the possibility of plugging the tubes by refrozen debris. Another purpose is to identify a critical penetration and estimate the influence of ex-vessel flooding on the tube behavior.

3.2. Approach

The study presented in section 2 was made using a 2-D conduction model, and including the initial debris flow-down period in the calculations. The initial flow-down period is specifically important in case of empty tube and large melt superheat, as it is in the CORVIS experiment 03/1. In case of small melt superheat and residual water in the tubes the flowdown period is expected to be short. Because of fast freezing inside the tubes the penetration depth will be small. It is likely that in the course of a severe accident the superheat of the melt in the lower plenum will be small. Relatively small superheat is also obtained in the results of the integrated codes, mentioned above. The present study is made assuming that the superheat is small and the initial flowdown period may be neglected. It is assumed that the melt at the initial temperature instantaneously fills the relatively small penetration depth. The earlier studies [Chavez, 1994], [Epstein, 1976], [Ostensen, 1973], indicate that the critical (highest) temperatures of the tube occur relatively close to the vessel compared to the penetration depth. Thus the heat conduction in the critical place occurs mainly in the radial direction. In the present analysis only the heat conduction in the radial direction was considered - 1-D conduction equation is solved.
3.3. Thermal Analysis

3.3.1. Model Description

A simplified 1-D conduction model is used to analyze all cases. The conduction model was solved numerically using the MELCOR code [Summers, 1994]. It should be noted however that for this analysis the default MELCOR model of the penetration tubes was not used. Instead the HS (Heat Structure) Package, and the CVH (Control Volume Hydrodynamics) Package were used to model the behavior of penetration tubes below the reactor vessel, filled by hot molten debris. Penetration tubes were modeled using heat structures composed of two different materials; steel and debris. Heat transfer from tube surface to the drywell atmosphere (or water pool) was modeled using standard MELCOR heat transfer package. Both convection and radiation were taken into account.

3.3.2. Geometry Data

The three types of penetrations tubes: Instrument tube, CRD drive, and drain line are modeled. Geometry data was taken from GKN drawings [GKN]. Instrument tube, and drain line, are modeled as steel tubes with empty space into which debris can flow. The CRD tube geometry is more complex, with several internal tubes. To avoid complexity of the computational model, the model of this tube was simplified. The CRD guide tube (external) and one internal tube are modeled, with two cylindrical empty spaces into which debris can flow (figure 3.1).

![Figure 3.1. Geometry of penetration tubes.](image)

Table 3.1. Material thermal properties

<table>
<thead>
<tr>
<th>Material</th>
<th>Density $\text{kg/m}^3$</th>
<th>Specific heat $\text{J/kg/K}$</th>
<th>Thermal conductivity $\text{W/m/K}$</th>
<th>Heat of fusion $\text{J/kg}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Carbon steel</td>
<td>8000</td>
<td>450</td>
<td>35.0</td>
<td>-</td>
</tr>
<tr>
<td>Oxidic debris</td>
<td>8544</td>
<td>967</td>
<td>5.5</td>
<td>$3.2 \times 10^5$</td>
</tr>
<tr>
<td>Metallic debris</td>
<td>7640</td>
<td>676</td>
<td>31.8</td>
<td>$2.3 \times 10^5$</td>
</tr>
</tbody>
</table>

Table 3.2. Initial conditions for thermal analysis

<table>
<thead>
<tr>
<th>Case</th>
<th>Debris type</th>
<th>Debris temperature $\text{K}$</th>
<th>Crusting temperature $\text{K}$</th>
<th>Tube initial temperature $\text{K}$</th>
<th>External coolant</th>
<th>Coolant temperature $\text{K}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>metallic</td>
<td>1850</td>
<td>1800</td>
<td>703</td>
<td>steam</td>
<td>453</td>
</tr>
<tr>
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<td>1800</td>
<td>453</td>
<td>steam</td>
<td>453</td>
</tr>
<tr>
<td>3</td>
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<td>2657</td>
<td>2627</td>
<td>703</td>
<td>steam</td>
<td>453</td>
</tr>
<tr>
<td>4</td>
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<td>2657</td>
<td>2627</td>
<td>453</td>
<td>steam</td>
<td>453</td>
</tr>
<tr>
<td>5</td>
<td>metallic</td>
<td>1850</td>
<td>1800</td>
<td>453</td>
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<tr>
<td>6</td>
<td>oxidic</td>
<td>2657</td>
<td>2627</td>
<td>453</td>
<td>water</td>
<td>400</td>
</tr>
</tbody>
</table>
The CRD internal structures are not taken into account. For conservatism heat transfer from the inside debris layer to the internal structures is not modelled. Geometrical dimensions of all tubes are shown in figure 3.1.

3.3.3. Material Data

Three types of materials are included in the calculations: steel, metallic debris, oxidic debris. The thermal properties (density, thermal conductivity, specific heat) of those materials were assumed following reference [Leonard, 1992], and are shown in table 3.1. For all calculations the debris is assumed to be initially molten. During calculation it solidifies, releasing the heat of fusion. The heat of fusion was included in the calculations by specifying the material heat capacity in the melting temperature region as equal to the heat of fusion divided by the temperature difference between liquidus and solidus. This approach leads to a sharp change of the heat capacity function. Because of that sharp change small time step must be applied when solving the conduction equation by MELCOR.

3.3.4. Initial Conditions

Initial temperatures of the debris and the penetration tubes, as well as the temperatures of surrounding coolant (atmosphere or water) were selected based on the values that are expected to be encountered in the course of a severe accident. The values assumed for the analysis are discussed below, and summarized in table 3.2.

Initial conditions in the containment.

By the time the reactor core is degraded and starts slumping into the lower plenum the containment pressure is typically 3-4 bar. Atmospheric temperatures, obtained in an analysis made with an integrated severe accident code - MELCOR [McClure, 1992], were, at the time of the vessel failure, below 450 K. Two cases are considered in the analysis.
- The drywell is filled with steam at 453 K, 4 bar. Penetration tubes are cooled in this case by the following mechanisms: natural convection and thermal radiation.
- The drywell is filled with water at 400 K, which is slightly below the saturation temperature for the applied 4 bar pressure. Penetration tubes are cooled in this case by convection to the water pool.

Initial temperatures of penetration tubes.

Temperatures of the penetration tubes outside the vessel is expected to be close to the drywell atmosphere temperature. The upper part of the tubes, near the vessel lower head, may have higher temperature, close to the vessel head temperature. Therefore two cases are considered. The case with low initial temperature of the penetration tubes: initial temperature equal to 453 K. The case with high initial temperature of the penetration tubes: initial temperature equal to 703 K.

Initial temperature and composition of debris.

The analyses were performed for two types of debris:
- Metallic debris, crusting temperature of 1800 K, initial temperature 50 K above the crusting temperature.
- Oxidic debris, crusting temperature 2627 K, initial temperature 70 K above crusting temperature.

These debris compositions were selected to envelope possible debris bed compositions during severe accidents.

In the numerical calculations the initial node temperatures were set to the debris temperatures in case of all debris nodes, as well as the interfacial nodes. For example, in case of the drain line, the initial node temperatures are equal to the debris temperatures for nodes 1 through 9, inclusive (figure 3.1). Other nodes are assumed to be initially at the temperature equal to the "initial tube temperature", discussed above.

3.3.5. Boundary conditions.

At the outer surface of the penetration tubes heat is lost by convection and radiation. Heat transfer from the tube surface to the drywell atmosphere (or water pool) was modelled using standard MELCOR heat transfer package. This resulted in natural convection and radiation heat transfer, in cases of the steam cooling, and pool boiling heat transfer, in cases of the water cooling.

The decay heat was taken into account. Because the volume of the debris inside the tubes is very small, the decay heat power source in the debris has rather small influence on the results and need not to be known with great accuracy. Based on [Leonard, 1992] the power density is 0.29 - 0.44 MW/m². The value of 0.5 MW/m² was assumed as a conservative estimation.

3.3.6. Results of Thermal Analysis

Results of the thermal analysis are presented for the six cases shown in table 3.3. In each case the results for three different penetration tubes are presented - the instrument tube, the CRD tube, and the drain line. Typical temperature history plots are shown in figures 3.2 - 3.4. Figures 3.2 - 3.4 show drain line temperature histories for cases 1 (metallic debris), 3 (oxidic debris), and 5 (metallic debris, water cooled). Each of these figures shows four temperatures:
- center debris temperature (node no. 1),
- outer debris temperature (node no. 8),
- inner tube temperature, (node no. 10),
- outer tube temperature, (node no. 13).

The general behavior is very similar in all cases. The outside debris node temperature quickly decreases, while the inside node remains hot (above the melting
point) for a longer period of time. For example in the calculations shown in figure 3.3 the debris is totally frozen after more than 100 s. Temperatures of the penetration tubes increase, pass a maximum at 10 - 30 s, and then slowly decrease. The maximum temperatures are very different for different cases. The most severe cases are those when tubes are filled with oxidic debris, and cooled by steam. Temperatures of the drain line and the instrument tube reach, or even exceed, steel melting temperature.

In the cases in which drywell is filled with water the penetration tubes temperatures are lower. After an initial rise the temperatures of the tubes are quickly reduced because of relatively efficient external cooling. Temperature rise is very sharp and a peak value is reached relatively early (figure 3.4).

![Figure 3.2. Drain line temperatures, Case 1.](image)

![Figure 3.3. Drain line temperatures, Case 3.](image)

![Figure 3.4. Drain line temperatures, Case 5.](image)

Table 3.3. Maximum temperatures of penetration tubes (inner steel node).

<table>
<thead>
<tr>
<th>Case</th>
<th>Instrument tube</th>
<th>CRD drive</th>
<th>Drain line</th>
</tr>
</thead>
<tbody>
<tr>
<td>1 (Metallic, high temperature)</td>
<td>1500</td>
<td>1250</td>
<td>1600</td>
</tr>
<tr>
<td>2 (Metallic, low temperature)</td>
<td>1450</td>
<td>1160</td>
<td>1500</td>
</tr>
<tr>
<td>3 (Oxidic, high temperature)</td>
<td>1800</td>
<td>1700</td>
<td>1900</td>
</tr>
<tr>
<td>4 (Oxidic, low temperature)</td>
<td>1750</td>
<td>1600</td>
<td>1850</td>
</tr>
<tr>
<td>5 (Metallic, water cooled)</td>
<td>1150</td>
<td>1000</td>
<td>1200</td>
</tr>
<tr>
<td>6 (Oxidic, water cooled)</td>
<td>1300</td>
<td>1250</td>
<td>1400</td>
</tr>
</tbody>
</table>
3.4. Failure Analysis

To estimate whether the penetration tubes fail or not a simplified method was used. Three failure modes were considered: ultimate strength failure, melt down, and creep failure. Temperature histories were obtained from thermal analysis (tube inner node temperatures were used). The value of stress was assumed to be constant and was calculated using the formula [Young, 1989]:

\[ \sigma = \frac{P R}{\delta} \]

where: \( \sigma \) - equivalent stress, Pa,
\( P \) - pressure, Pa,
\( R \) - mean tube radius, m,
\( \delta \) - tube thickness, m.

Total pressure inside the tubes was estimated taking into account hydrostatic heat of molten debris and water in the RPV, as well as the pressure difference between RPV and containment. Two cases were considered: depressurized vessel (no RPV-containment pressure difference) and high pressure vessel. The values of stress for those cases are shown in table 3.4.

### 3.4.1. Failure Modes

Three failure modes were considered: ultimate strength failure, melt down, and creep rupture. The data was taken from [Rempe, 1993].

**Ultimate strength**

The data on ultimate strength (reproduced from [Rempe, 1993]) is shown in table 3.5. Failure is assumed to occur if the value of stress exceeds the ultimate strength for the current temperature.

### Melt down

The melt down temperatures of the carbon and the stainless steel were taken from [Rempe, 1993], and are equal to 1789 K for carbon steel, 1671 K for stainless steel. Failure is assumed to occur when the current temperature exceeds the melting temperature.

### Creep rupture

The creep failure is modeled using the Larson-Miller parameter, \( P_{LM} \). The time to rupture \( t_r \) is calculated as a function of the temperature \( T \), and stress \( \sigma \):

\[ \log_{10}(t_r) = \frac{P_{LM}}{T} - C \]

\[ P_{LM} = A - B \log_{10}(\sigma) \]

where:
\( t_r \) - time to rupture, s,
\( T \) - temperature, K,
\( \sigma \) - stress, Pa,
\( P_{LM} \) - Larson-Miller parameter,
A, B, C - material dependent constants.

The material constants A, B, C, were taken from [Rempe, 1993]. The equations are based on experimental data obtained at INEL. The equations presented in [Rempe, 1993] were converted to SI units to give:

For the carbon steel:

\[ \log_{10}(t_r [s]) = \frac{P_{LM}}{T [K]} - 9.44 \]

\[ P_{LM} = 46130 - 4238 \log_{10}(\sigma [Pa]) \]

For the stainless steel:

\[ \log_{10}(t_r [s]) = \frac{P_{LM}}{T [K]} - 12.44 \]

\[ P_{LM} = 58770 - 5086 \log_{10}(\sigma [Pa]) \]

### Table 3.4. Values of stress assumed for the penetration tubes.

<table>
<thead>
<tr>
<th>Tube type</th>
<th>( R, [m] )</th>
<th>( \delta, [m] )</th>
<th>( \sigma, [MPa] )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Instrument tube</td>
<td>0.0228</td>
<td>0.0076</td>
<td>0.5 1.5</td>
</tr>
<tr>
<td>CRD tube</td>
<td>0.0365</td>
<td>0.0130</td>
<td>0.5 2.4</td>
</tr>
<tr>
<td>Drain line</td>
<td>0.0215</td>
<td>0.0050</td>
<td>0.5 2.2</td>
</tr>
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</table>

### Table 3.5. Ultimate strength data.

<table>
<thead>
<tr>
<th>Carbon steel,</th>
<th>T, K</th>
<th>( \sigma_u, [MPa] )</th>
<th>297</th>
<th>500</th>
<th>700</th>
<th>800</th>
<th>900</th>
<th>1000</th>
<th>1050</th>
<th>1100</th>
<th>1150</th>
</tr>
</thead>
<tbody>
<tr>
<td>SA106B</td>
<td></td>
<td>550</td>
<td>598</td>
<td>467</td>
<td>324</td>
<td>169</td>
<td>83</td>
<td>74</td>
<td>75</td>
<td>60</td>
<td></td>
</tr>
<tr>
<td>Stainless steel</td>
<td>T, K</td>
<td>( \sigma_u, [MPa] )</td>
<td>297</td>
<td>977</td>
<td>1050</td>
<td>1100</td>
<td>1150</td>
<td>1200</td>
<td>1300</td>
<td>1373</td>
<td></td>
</tr>
<tr>
<td>304SS</td>
<td></td>
<td>642</td>
<td>285</td>
<td>197</td>
<td>147</td>
<td>109</td>
<td>73</td>
<td>40</td>
<td>30</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

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Analysis of Reactor Vessel Lower Head Penetration Tube Failure

<table>
<thead>
<tr>
<th>Case</th>
<th>Instrument tube</th>
<th>CRD drive tube</th>
<th>Drain line</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>low pressure</td>
<td>high pressure</td>
<td>low pressure</td>
</tr>
<tr>
<td>1 (Metallic, high temperature)</td>
<td>OK Failure</td>
<td>OK OK Failure</td>
<td>Failure Failure</td>
</tr>
<tr>
<td>2 (Metallic, low temperature)</td>
<td>OK Failure</td>
<td>OK OK Failure</td>
<td>Failure Failure</td>
</tr>
<tr>
<td>3 (Oxidic, high temperature)</td>
<td>Failure Failure</td>
<td>Failure Failure</td>
<td>Failure Failure</td>
</tr>
<tr>
<td>4 (Oxidic, low temperature)</td>
<td>Failure Failure</td>
<td>Failure Failure</td>
<td>Failure Failure</td>
</tr>
<tr>
<td>5 (Metallic, water cooled)</td>
<td>OK OK</td>
<td>OK OK</td>
<td>OK Failure</td>
</tr>
<tr>
<td>6 (Oxidic, water cooled)</td>
<td>OK OK</td>
<td>OK OK</td>
<td>OK Failure</td>
</tr>
</tbody>
</table>

Using the above equations the time to rupture, \( t_r \), is calculated at every step time for current conditions. The damage function, \( D(t) \), is defined as the inverse of \( t_r \). The cumulative damage, \( CD(t) \), is calculated from:

\[
CD(t) = \int_0^t D(t) \, dt = \int \frac{dt}{(t/T_0)}
\]

Failure is assumed to occur if \( CD(t) \geq 1.0 \).

3.4.2. Results of Failure Analysis

Results of the failure analysis are presented in Table 3.6. The investigation of these results lead to the following conclusions:

The critical penetration for GKN reactor is the drain line. The reason is a relatively small thickness of this tube (Figure 3.1) compared to the other penetration tubes.

In case when water is outside the tubes the failure was not calculated to occur except for the case of the drain line at high vessel pressure. Thus external flooding may promote plugging of the penetration tubes and prevent an early vessel failure.

3.5. Conclusions from GKN Analysis

Thermal response of GKN Dovedaard penetration tubes to two types of debris: metallic and oxidic, was analyzed, assuming that the initial superheat of the debris is relatively small (below 100 K, compared to for example CORVIS 03/1 tests, where the superheat was as high as about 900 K). The main purpose was to estimate whether the penetration tubes can withstand the first attack of the melt and prevent an early vessel failure, when the lower plenum still contains substantial amount of water. The obtained results lead to the following conclusions.

The critical penetration of the GKN vessel, which is most likely to fail if a molten debris relocates into the tube, is the drain line. In case of high internal vessel pressure failure was calculated for all considered cases.

External flooding may provide a good way to help the tubes to withstand the first attack of the melt. In the cases with the external flooding no failure was calculated, except for the drain line in case of high vessel pressure.

In case of metallic debris, low vessel pressure, and low initial temperature of the tubes, none of the penetration tubes were calculated to fail. If a scenario leading to such conditions is analyzed with MELCOR, the code will predict an early vessel failure. This indicates that the modelling approach taken in MELCOR may be too conservative. On the other hand, due to uncertainties involved in the melt progression one cannot be sure that in case of a real accident an early vessel failure would be avoided. Therefore the MELCOR model seems to be a reasonable conservative approach in the situation when the outcome is highly uncertain.

The general conclusions from this study, although obtained for the GKN Dovedaard reactor type, are also expected to apply to other BWR types.

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Thermal Hydraulic and Mechanical Aspects of In-Vessel Retention of Core Debris

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Abstract

Several advanced water reactor designs incorporate provisions to manage the escaped core debris outside the reactor vessel. This type of ex-vessel management approach involves a difficult task of core debris cooling, direct containment heating and steam explosion control inside the containment building. If one can secure the integrity of the lower head by maintaining coolability within the reactor, core meltdown accidents will be arrested within the vessel at a significant advantage over the ex-vessel measures. This paper examines potential options for in-vessel retention for large PWRs as functions of time spent for the wetting of the external wall of the lower head. Several important options were evaluated utilizing detailed thermohydraulic and mechanical analysis models that have been developed and benchmarked against experimental data. Results elucidate that the risk of lower head failure by either creep rupture or quench fracture increases rapidly with the amount of time spent till external wall wetting. To expedite rapid wetting of the lower head without installation of major pumping systems and large inventory of cooling water, gap structures have been proposed inside and outside the reactor vessel lower head. The external gap structure, designated as COASISO, is shown to be an effective tool for in-vessel retention of core melt for both new and operating reactors during a hypothetical severe accident.

I. Introduction

Following the TMI-2 accident, advanced water reactor designs have been evolving with the emphasis on severe accident management capability. Among these are the designs incorporating the ex-vessel core debris cooling and in-vessel retention capabilities. The ex-vessel management approach, however, is observed to carry several shortcomings. For example, the core melt that is ejected through a failed reactor pressure vessel (RPV) lower head opening tends to agglomerate jeopardizing the long-term coolability. In addition, the issues associated with the containment direct heating and the steam explosion are yet to be resolved. For this reason, more recent designs appear to prefer the in-vessel retention of core debris by maintaining the structural integrity of the reactor vessel lower head during a core meltdown accident.

The in-vessel retention can be achieved by the full flooding of reactor cavity to cool the external wall of the lower head, whereby avoiding the structural failure by creep rupture. The application of this approach to large power reactors is not trivial because of relatively short time between the detection of core melting and the lower head failure. Therefore, special design features to facilitate rapid flooding are essential to the success of in-vessel retention. We have examined the thermal and structural behavior of a large power reactor during a severe accident for the full cavity flooding approach. Based on the result, an improved in-vessel retention method using gap structures has been proposed for rapid cooling of the lower head, as described herein.
II. Analysis Method

The present work analyzes three design approaches including 1) ex-vessel core debris cooling, 2) in-vessel retention by slow passive cavity flooding, and 3) the lower head cooling by rapid forced flooding, and compares the advantages and disadvantages from the standpoint of severe accident management for a large PWR. The Korean Standard Nuclear Power Plant (KSNPP), a 1000 MWe PWR [1] modified from Combustion Engineering’s System 80 [2], is taken as the reference design in this study. The station blackout accident is selected as the most severe scenario with respect to the structural integrity of the lower head. MAAP4 is applied to obtain temperature and pressure history for a given accident scenario [3-6].

The thermohydraulic model represents the extent of debris jet disintegration by a jet-water entrainment model, which can result in two types of debris configurations. One is particulate debris that eventually quenches in the water as a result of the entrainment process, and the remainder of the debris penetrates to the bottom of the lower plenum and collects as a continuous layer. Each is treated as a separate region and has governing principles for its behavior. The potential for creating gap (contact) resistance and boiling heat removal is considered for heat transfer between the debris bed, the reactor vessel wall and steel structures, and most importantly the vessel-to-crust gap water. The proposed in-vessel cooling mechanism due to material creep and water ingestion into the expanding gap between the core debris and the vessel wall was found to explain the non-failure of the TMI-2 vessel in the course of the accident. The particulate debris bed is a mixture of metal and oxide, which is distributed as individual spherical particles of sizes determined at the time of entrainment. Energy is received from the continuum bed below by radiation and convection. The continuum debris bed is described by the crust behavior with the heat flux to the crust given by the natural convection correlations relating the Nusselt and Rayleigh numbers for the central region of debris. Using these governing principles, the rate laws for heat and mass transfer are formulated for each type of debris condition in the lower plenum. With the integration of the individual rates, the formation, growth, and possible shrinkage of these regions are calculated. The potential reactor vessel breach is accounted for considering the combined thermal and mechanical response of the vessel wall.

Earlier investigators on the issue assumed that the lower head could fail if the wall heat flux exceeds the critical heat flux in the water pool. This assumption has no physical ground since the heavy walled vessel can maintain its integrity even under the film boiling condition if the internal pressure is not excessive. The structural integrity should be determined based on the margins against two expected modes of lower head failure: the creep rupture at elevated temperature and the brittle fracture initiated from existing cracks during rapid cooling from high temperatures.

For creep rupture prediction, an elastic-viscoelastic structural analysis was performed using ABAQUS-based model that has been benchmarked against a set of lower head failure test data, as described in the earlier paper [7]. To examine the risk of brittle fracture, a semi-circular crack with a depth of one quarter of the lower head wall thickness is postulated to exist at the bottom center at the lower head after the procedure for the analysis of pressurized thermal shock of a RPV. The temperature history of the lower head including crack front was calculated using ABAQUS. The elastic-plastic stress analysis was made for the temperature history. The crack tip stress intensity factor was then determined from Raju-Newman’s correlation [8]. The lower head integrity is examined by comparing the maximum stress intensity factor and the fracture toughness of the RPV steel.
III. Results and Discussion

Ex-Vessel Debris Cooling Option

The reference design of KSNPP assumes that the lower head fails during a severe accident. A provision is made for the cooling of escaped core debris on the reactor cavity floor using fire hydrant. Hence the reference KSNPP design has provisions to employ the ex-vessel management option. MAAP analysis has been performed for the station blackout scenario of the KSNPP. It is shown in Figure 1 that the molten core relocates to the lower plenum to cause the RPV lower head to heat up at 166 min. into the accident followed immediately by the core depressurization. Using the thermohydraulic history, the structural analysis was made to predict the creep displacement and the creep usage factor of the lower head, as shown in Figure 2. The lower head is predicted to fail at about 210 min. into the accident when the reactor pressure is still high enough to result in core debris ejection into the reactor cavity. If the reactor cavity is partially flooded from the fire hydrant injected for ex-vessel debris cooling, the steam explosion and direct containment heating are expected to take place, as depicted in Figure 3. Therefore provisions to cope with these consequences are required in addition to the measures for core melt spreading on the reactor cavity if a severe accident is to be successfully managed in the reference design of KSNPP.

In-Vessel Retention by Slow Full Cavity Flooding

For the development of an advanced Korean PWR, designated as the Korean Next Generation Reactor (KNGR), the in-vessel retention approach is being actively pursued by using the full cavity flooding option. The operator is expected to declare a core melting when the core exit gas temperature reaches 1093 °C and then the full cavity flooding is initiated. In the early stage of the study, the cavity flooding was planned to use a gravity-driven water supply from a reservoir inside the containment. To a first approximation we assumed the full cavity flooding to take 37 min. from the relocation.

Using the slow flooding scenario, the lower head behavior is predicted. The heat transfer coefficient between the lower head and the flooded water is determined from the experimental data, as collected in Figure 4. At the subcritical heat flux, the data from the CYBL experiment was employed whereas a film boiling heat transfer coefficient of 630 W/m²K measured by Jeong [9] was used for the post-critical heat flux regime. The MAAP analysis result for the scenario showed that the core melting indication develops at about 10 min. before the core debris relocation into the lower plenum, as shown in Figure 5. The inner surface of lower head begins to heat up at about 166 min. when the core debris relocation starts. By the gravity-driven flooding, the lower head cooling is expected to begin at 203 min. into the accident. Since the creep rupture was predicted at 210 min. otherwise, the lower head is saved by only several minutes of time margin. Therefore the slow flooding is not considered to ensure adequate margin against the high temperature failure mode.

For fracture analysis, the temperature history of the lower head and the crack area was determined as shown in Figures 5 and 6. The risk of brittle fracture due to thermal shock was determined from the calculated stress intensity factor, as shown in Figure 7. Two peaks of $K_i$ are observed each with the value of about 100 MPa m$^{1/2}$. The first peak during the heatup does not present the risk of fracture as the fracture toughness of the initial vessel material is over 200 MPa m$^{1/2}$. The maximum crack front temperature approached the ferritic-austenitic phase transformation temperature of 723 °C during the heatup. The fracture toughness of the material can significantly be decreased if the material is quenched from temperatures above the phase transformation temperature. Taking into account the small temperature margin to the phase transformation at the crack front, the risk of brittle fracture during the quenching cannot be
Given the conclusion of the analysis that the risk of lower head failure is not negligible for the case of slow flooding option, the core debris ejection into the flooded cavity needs to be considered. As shown in Figure 8, the core debris-water reaction in full-flooded cavity may cause steam explosion to exert direct physical impact on the RPV in addition to the charge onto the containment wall. Therefore the slow full cavity flooding approach is highly undesirable from the accident management point of view.

**In-Vessel Retention by Rapid Full Cavity Flooding**

Knowing that the risk of lower head failure increases with the time from the core debris relocation, the rapid wetting of lower head is essential for the success of the in-vessel retention. Rapid full cavity flooding is feasible by using high capacity water supply systems. In this study, a forced pumping system is postulated to achieve wetting of the lower head with only 7 min. delay from the core debris relocation point.

Based on the rapid flooding scenario, the thermal analysis predicts that the lower head remains cooled at the outer surface even though the inner wall is heated upon the core debris relocation, as shown in Figures 9. The chances for the creep rupture are also shown to be very low in Figure 10. The thermal and fracture mechanics analysis for the semi-circular crack with a depth of one quarter of wall thickness is shown in Figures 11 and 12. Since the temperature change in the crack area is small, the fracture toughness is expected to remain at the initial high value compared with the predicted maximum stress intensity factor from the cooling. Therefore, the rapid flooding of the lower head can assure the in-vessel retention for the large PWR.

If the entire reactor cavity is to be flooded to wet the lower head in such a short period of time, a high capacity pumping system and a means for expediting water ingress through thermal insulation will be required. High capacity pumping system will be costly to install and will require a large emergency power supply. Furthermore, the reliable means to allow for rapid water penetration through the RPV insulation may involve additional design complexity and cost.

**Rapid Lower Head Cooling by Engineered Gap Flooding**

Rapid lower head cooling can be achieved by using gap structures designated as the COrium Attack Syndrome Immunization Structures (COASIS) that provide for coolable gap geometry for the lower head inner surface (COASISI) and the outer surface (COASISO) inside the RPV insulation layers, as shown in Figure 13. COASISO allows for a rapid flooding of the narrow gap space by means of small pumping system and/or by fire hydrant available in the KSNPP reference design. As the water supply line bypasses the lower head insulation, there is virtually no time delay in the flooding from the operator action. After the COASISO is flooded the continued water supply will spill over into the reactor cavity. The long-term cooling of lower head can be achieved by recirculating the reactor cavity water using a small capacity pumping system. In this situation the outer wall of the lower head will stay wetted over the entire period of the core-melting accident and will ensure its structural integrity throughout the accident.

COASISI can be used to provide additional protection of the lower head from the inside, especially for the penetration nozzle weld areas. If COASISI is chosen not to be used from simplicity and maintenance considerations, the penetration nozzle weld can be damaged by either ablation or creep which may lead to the nozzle release and subsequent core debris ejection through the hole. In order to prevent this from happening, COASISO is designed also to function as a stopper for the loosened nozzle as illustrated in Figure 14.

**IV. Summary and Conclusions**

Severe accident management strategy for water reactors has evolved from the ex-
vessel debris cooling approach to the in-vessel retention. The in-vessel retention can be achieved by avoiding structural failure of the lower head. Often the critical heat flux is used as the limit for the structural integrity, without valid physical grounds. This study has illustrated that the lower head integrity can be maintained even under the film boiling condition if the internal pressure is not excessive. On this ground creep rupture and brittle fracture are suggested as meaningful criteria for use in the in-vessel retention approach.

The time lapse from the core debris relocation into lower plenum to the lower head cooling is demonstrated to be the critical variable of the in-vessel retention by the thermal and structural analysis using MAAP and ABAQUS. The slow flooding of the reactor cavity is not desirable due to the risk of creep rupture and brittle fracture. Rapid flooding with 7 min. delay from the core debris relocation is shown to provide adequate assurance for the lower head integrity and hence for the in-vessel retention. Such a rapid flooding of the full cavity is expected to require a major water supply system.

The engineered gap structures named as COASIS are proposed as an effective means for rapid cooling of the lower head during a severe accident. The external gap structure applied inside the RPV insulation, COASISO, will allow for the rapid flooding with a fire hydrant system and/or small capacity pumping system. To cope with the nozzle weld failure at the lower head inner surface, COASISO is designed to function also as the nozzle protector.

Acknowledgment

The authors acknowledge the financial and technical support provided for this work by the Korea Electric Power Research Institute as part of the KNGR development program.

References

Figure 1. Temperature and pressure histories of the lower head for a station blackout accident as predicted by MAAP4

Figure 2. Displacement and creep usage factor of lower head for a station blackout accident as predicted by ABAQUS-based model

Figure 3. Predicted lower head behavior in the reference design during a station blackout accident

Figure 4. Ex-vessel heat transfer coefficients used for lower head cooling analysis

Figure 5. Lower head temperature history for slow flooding scenario

Figure 6. Outer wall crack front temperature for slow flooding scenario

Figure 7. Crack tip stress intensity factor predicted for slow flooding scenario

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Figure 8. Predicted lower head behavior for slow flooding scenario

Figure 9. Temperature and pressure history of lower head for rapid flooding scenario as predicted by MAAP4

Figure 10. Displacement and creep usage factor for rapid flooding scenario as predicted by ABAQUS-based model

Figure 11. Outer wall crack front temperature for rapid flooding scenario

Figure 12. Crack tip stress intensity factor predicted for rapid flooding scenario

Figure 13. Design schematic of Corium Attack Syndrome Immunization Structures (COASIS)

Figure 14. COASISO nozzle stop design
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