TMI-2 Analysis Exercise
Final Report

TMI-2 Analysis Exercise Task Group

Principal Working Group No. 2
Committee on the Safety of Nuclear Installations
OECD - Nuclear Energy Agency
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Acronyms

AFW  auxiliary feedwater
BCD  Battelle Laboratories, Columbus Division
CEA  Commissariat A L'Energie Atomique
CSNI  Committee on the Safety of Nuclear Installations
CTN  Catedra De Tecnologia Nuclear E.T.S. Ingenieros Industrialles Universiadd Politecnica De Madrid
DOE  United States Department of Energy
ECN  Netherlands Energy Research Foundation
EPRI  Electric Power Research Institute
FAI  Fauske and Associates
GRS  Gesellschaft fur Reaktorsicherheit mbH
HPI  high pressure injection
INEL  Idaho National Engineering Laboratory
JAERI  Japan Atomic Energy Research Institute
JINS  Japan Institute for Nuclear Safety
MFW  main feedwater
MSIV  main steam isolation valve
NEA  Nuclear Energy Agency
OECD  Organization for Economic Cooperation and Development
OTSG  once through steam generator
PORV  power operated relief valve
RCS  reactor coolant system, or primary coolant system
SBLOCA  small break loss of coolant accident
SNL  Sandia National Laboratories
TMI-2  Three Mile Island Unit 2
UKAEA  United Kingdom Atomic Energy Agency
USNRC  United States Nuclear Regulatory Commission
VTT  Technical Research Center of Finland
Introduction

On March 28, 1979 at the Three Mile Island Unit 2 (TMI-2) nuclear power plant led to a melting of approximately 50% of the reactor core. Since severe accidents cannot be simulated at or near full scale, TMI-2 provides a unique opportunity to assess the capability of current severe accident analysis methods to simulate an accident in a full scale nuclear power plant. In this regard the OECD Nuclear Energy Agency (NEA), in collaboration with the U.S. Department of Energy (DOE), has established a Joint Task Group to analyze various periods of the accident in an effort to benchmark severe accident computer codes. EG&G Idaho, under contract to DOE, prepared and qualified a data base to provide the initial and boundary conditions during 1986 and 1987. Initial calculations by the participants became available in 1988. Participants in the analysis exercise and contributors to this report are are shown in Table 1. The results and conclusions that may be drawn from these results of the benchmark analyses are documented in this report. Brief descriptions of the codes are included in the participant’s submittals reproduced in the appendices.

Table 1. TMI-2 analysis exercise participants.

<table>
<thead>
<tr>
<th>Country (Agency)</th>
<th>Code(s) Used</th>
</tr>
</thead>
<tbody>
<tr>
<td>Federal Republic of Germany (GRS)</td>
<td>ATHLET (D)a</td>
</tr>
<tr>
<td>Finland (VTT)</td>
<td>MAAP 3.0 (EU)b &amp; RELAP5 (EU)</td>
</tr>
<tr>
<td>France (CEA)</td>
<td>CATHARE1/ICARE (D)b</td>
</tr>
<tr>
<td>Italy (ENEA)</td>
<td>SCDAP/RELAP5/MOD0/VER47 (EU)</td>
</tr>
<tr>
<td>Japan (JAERI)</td>
<td>THALES (D) &amp; SCDAP (EU)</td>
</tr>
<tr>
<td>(JINS)</td>
<td>SHAPE (D)</td>
</tr>
<tr>
<td>Netherlands (ECN/Pen)</td>
<td>MARCH (EU)</td>
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<tr>
<td>Spain (CTN/University of Madrid)</td>
<td>MARCH 3/CTN1 (D)</td>
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<tr>
<td>United Kingdom (UKAEA)</td>
<td>MELPROG (EU) &amp; FLOW3D (D)</td>
</tr>
<tr>
<td>United States (USNRC/BCD)</td>
<td>MARCH 3/Experimental (D)</td>
</tr>
<tr>
<td>(EPR/FAI)</td>
<td>MAAP 3B revision 12 (D)</td>
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<td>(DOE/INEL)</td>
<td>SCDAP/RELAP5/MOD1/VER05 (D)</td>
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<tr>
<td>(USNRC/SNL)</td>
<td>MELCOR (D)</td>
</tr>
</tbody>
</table>

a. D = Primary developer of a code.
b. EU = End user of a code.
The primary objectives of the analysis exercise are to (a) assess the capability of severe accident analysis computer codes to simulate severe accidents, and (b) establish an international consensus on severe accident analysis capabilities. While the TMI-2 accident put certain limitations on the precision with which these objectives can be achieved. Although the operational sequence of events is well known, times for phenomenological events, such as initial cladding relocation, are based on indirect data,\textsuperscript{a} core end state conditions, and analyses.\textsuperscript{b} A second limitation is the lack of recorded boundary flows. Since there were no direct, recorded measurements of the boundary flows, it was necessary to estimate the flows by analysis of indirect measurements. Thus, two limitations on our conclusions are the necessity to infer phenomenological event timing and the necessity to infer boundary conditions from indirect data.

Recognizing the limitations stated above, the approach to the analysis exercise was to agree upon a base case accident scenario and boundary conditions. Parametric calculations were then made to determine the effect of varying the boundary conditions. In this manner, the analysis exercise does not simulate exactly the TMI-2 accident, but a best estimate and parametric variations of the accident. However, there is a common basis for comparison of the codes to each other and the accident data.

In interpreting such comparison, it should be noted that a broad spectrum of codes, developed with various objectives and for different purposes, were used by the participants in this exercise. In general, these codes can be grouped into three categories:

1. Thermal hydraulic codes, with detailed thermal hydraulic models. These codes were often developed for design basis safety analyses, and have been amended to extend their applicability into the severe accident regime (e.g., ATLHET, CATHARE/ICARE, SCDAP/RELAP).

2. Severe accident risk codes, with simple thermal hydraulic models, developed primarily for quick-running parametric studies for use in probabilistic risk assessments (e.g., MARCH, MELCOR, MAAP, THALES).

\textsuperscript{a} Indirect data include plant data recorded during the accident that provide an indication of an event or measurement. For example, the occurrence of fuel rod cladding breach failure is indicated by radiation alarms, and the in-core self-powered neutron detectors have been used to estimate in-core temperatures at discrete times during the accident (see Reference 1).

\textsuperscript{b} Analyses, independent of the analysis exercise, provide a means to estimate quantitative behavior during the accident. For example, the containment pressure response has been used to estimate the quantity of hydrogen generated during the accident.
3. Special purpose codes, which are not intended to provide an integral analysis of accident progression, but an examination of specific phenomena or segments of the accident progression (e.g., FLOW3D for natural circulation estimates, SHAPE, MACRES for fission product analyses).

For this reason, the results of this exercise cannot be used to rank the codes, or select the "best" code. Instead, conclusions are drawn for the codes as a group, and our overall ability to reproduce the TMI-2 observations.

The following sections of this report contain summaries of the event sequence and accident scenario, the boundary conditions for the base case analysis, a comparison of the participant's results, and the conclusions that may be drawn from the TMI-2 analysis exercise. Reports on the analyses performed by the participants are contained in the appendices, along with descriptions of the codes used for their analyses.
Sequence of Events

For the analysis exercise, the TMI-2 accident is partitioned into four phases. Our best understanding of the sequence of events and important phenomena are discussed in this section. A more detailed presentation of the accident sequence and phenomenology is provided in the accident scenario.\(^1\) The four phases are shown relative to the measured reactor coolant system (RCS) pressure in Figure 1. Phase 1 of the accident covers the time period from accident initiation (0 min) to shutdown of the last RCS coolant pump (100 min). Phase 2 (100 to 174 min) covers core uncovering, and the initial heatup and melting of the reactor core. During Phase 3 (174 to 200 min) the core was recovered by running the 2B RCS coolant pump, and the core reheat that followed. During phase 4, high pressure safety injection was initiated, and the core was refilled. Relocation of molten core material to the lower plenum occurred resulting in a coolable geometry.

![Diagram showing pressure readings over time](image)

**Figure 1.** TMI-2 system pressures.

The TMI-2 accident was initiated by the inadvertent valve closure in the condensate polishing system. This valve closure led to trips of the condensate and main feedwater pumps, and the turbine. By plant design, reactor trip occurs on over-pressure; the reactor continued to produce power at the same level as before the secondary side trips. Until RCS pressure increases the reactor trip setpoint, the reactor continues to produce full power. The once through steam generator’s (OTSG’s) heat removal capability, having been significantly reduced by the loss of main feedwater,
caused the RCS pressure to increase (see Figure 2). In accordance with plant design, the power operated relief valve (PORV) opened at about 2 to 3 s decreasing the rate of RCS pressure increase. At 8 s, RCS pressure reached 16.3 MPa (2345 psia) causing a reactor trip. At this time, RCS average temperature, liquid volume, had therefore RCS pressure and pressurizer level all started to decrease. In anticipation of a low pressurizer level, the operators terminated letdown flow and initiated makeup flow at 25 l/s (400 gpm) to control pressurizer level. Indeed the pressurizer level respond to the makeup flow and started to increase. The events described have been the normal plant response to main feedwater and turbine trips.

![Graph showing RCS pressure, Hot Leg Temperature, and Saturation Temperature over time.](image)

**Figure 2.** RCS pressure compared to A-loop hot leg temperature and saturation temperature.

Within the sequence of normal plant responses to the initiating event, there were two events that did not take place. Although the auxiliary feedwater (AFW) pumps started in response to the main feedwater trip, block valves within the AFW piping system had been left closed prior to plant startup. The closed block valves prevented flow of AFW to the OTSGs. As a result of the lack of AFW flow, the OTSGs boiled dry in less than 2 min. Thus, the OTSGs no longer had the capability to remove decay heat from the RCS. The average RCS temperature started to increase instead of continuing to decrease. Therefore, RCS liquid volume was increasing due to both the decrease in liquid density and the inflow of high pressure injection (HPI). Safety injection was bypassed at 3.2 min. At 3.5 min, a pressurizer high level alarm occurred and pressurizer level continued to increase. Makeup flow was throttled at ~4.6 min causing the RCS average temperature to increase more rapidly. Between 5 and 6 min, the hot legs reached saturation, and the pressurizer level reached the top measurement tap. At 8 min, the operators noticed the
closed AFW block valves. After the operators opened the block valves, the average RCS temperature started to decrease.

The loss of secondary side heat removal was not the only contributor to this sequence. The second event was a failure of the PORV to close when the close signal was generated shortly after the reactor trip. The stuck-open PORV allowed RCS pressure to decrease until saturation was reached between 5 and 6 min. Once the RCS reached saturation, RCS pressure was determined by RCS temperature. At about 25 min, RCS temperature approached secondary side temperature, and therefore RCS pressure approached secondary side pressure. As long as the OTSGs removed the decay heat generated in the reactor core, RCS pressure was primarily determined by the overall steam generator heat transfer coefficient.\(^a\) The stuck-open PORV constituted a small break loss of coolant accident (SBLOCA). RCS coolant inventory continued to be depleted throughout Phase 1 of the accident. At 74 min, RCS coolant inventory had been depleted to a level that the B-loop RCS pumps were shut down due to vibration. At about 100 min, the A-loop RCS pumps were shut down for the same reason. Shutdown of the A-loop RCS pumps closed Phase 1 of the accident. In summary Phase 1 of the accident is characterized by accident initiation, a short-term loss of heat sink, and an SBLOCA.

Phase 1 of the accident is primarily controlled by RCS to secondary side heat transfer and the boundary flows (letdown, makeup/HPI, and PORV) as described above. Thus, the code models for heat transfer and critical flow (PORV) are the primary models exercised by simulating Phase 1 of the accident. For the analysis exercise the interest in Phase 1 has been in general to provide a set of initial conditions to begin Phase 2 of the accident simulation.

During Phase 2, the SBLOCA continued, but without the aid of the RCS pumps to pump coolant to the steam generators. Superheated steam appeared in the A-loop at about 110 min, thus indicating the core had uncovered. At ~125 min, RCS pressure started to increase (the PORV and the PORV block valve were still open) while the A-loop secondary pressure continued to decrease (Figure 1). The phenomena that may have caused RCS pressure to begin increasing are (a) an increase in the energy generation rate in the core due to oxidation of the fuel cladding, (b) the rate of energy transfer to the secondaries decreased due to the presence of hydrogen from cladding oxidation or a decrease in the flow rate of steam to the OTSGs probably due to the presence of hydrogen, or (c) a combination of both increased energy generation and decreased energy transfer to the secondaries. The subjective conclusion, based on the pressure behavior, is that cladding oxidation commenced at approximately 125 min. Cladding oxidation provided the primary source of energy for core heatup throughout Phase 2 of the accident. By the end of Phase 2, ~300 kg of hydrogen had been generated by oxidation, or 30% of the cladding had been oxidized.\(^2,3\)

\(^a\) The heat transfer coefficient determines the temperature difference between the RCS and secondary sides for a given heat flux.
At about 139 min (about the mid point of Phase 2), the PORV block valve was closed terminating the outflow through the PORV, but letdown flow continued. This reduced the LOCA, but was insufficient to reestablish a liquid level above the top of the core.\(^a\) Core heatup progressed to cladding melt, fuel dissolution, and relocation. A crust composed of previously molten metallic materials formed in the lower region of the core. This crust formed a crucible that collected additional molten material (a mixture of oxygen, uranium, and zirconium) as the molten material flowed downward in the reactor’s core. By 174 min, a significant blockage had been formed in the central region of the reactor’s core. The core conditions believed to exist at the end of Phase 2 are illustrated in Figure 3.

Phase 2 of the accident is controlled primarily by the boundary flows, cladding oxidation, melting and relocation of cladding, dissolution of UO2 by molten cladding, freezing of molten materials, crucible formation, effects of hydrogen on condensation in the steam generators and on primary-to-secondary heat transfer, and the effects of core geometry changes on thermal-hydraulics. Simulation of Phase 2 of the accident requires models to describe these phenomena and processes.

Phase 3 commences at 174 min with the 2-B RCS pump restart, hereafter called the pump transient. The pump transient is believed to have pumped ~30 m\(^3\) (1000 ft\(^3\)) into the reactor vessel. Eleven peripheral in-core thermocouples indicated that temperatures in the periphery of the core decreased to less than the high temperature alarm set point. From the in-core thermocouple response, it has been concluded that (a) a blockage had been formed in the center of the core prior to 174 min as shown in Figure 3, (b) the central region of the core was not significantly cooled, (c) the core temporarily refilled, and (d) the fuel rods in the upper center region of the core shattered due to embrittlement and the mechanical loads of the pump transient. Kuan,\(^2\) based on an analysis of the energy transfers during the pump transient concluded that ~160 kg of hydrogen were generated by oxidation of fuel rod cladding in the upper, center regions of the core during the pump transient. The hypothesized core conditions after the 2B RCS pump had been restarted are shown in Figure 4.

At about 224 to 226 min, it is believed that the crucible containing the molten core materials failed, allowing ~20 tons of molten core material to flow into the lower plenum. The best estimate accident scenario describes two relocation paths. When the crust surrounding the molten corium failed, it is believed that the material flowed radially outward, impinging on the core baffle plates with some material flowing downward through the peripheral fuel assembly in the southeast corner. Ablation of the baffle plate by the impinging molten corium created a hole and hence a path for flow into the core bypass region. The flow of molten material into the bypass region resulted in azimuthal redistribution of the molten corium. Molten corium flowed downward in the bypass region through

\(^a\) The minimum core water level during the accident is estimated to have been approximately 0.7 m (2 ft) based on the core end-state conditions.
Figure 3. Core geometry prior to 2-B RCS pump transient.
Figure 4. Core geometry after 2-B RCS pump transient.
holes in the core former plates (horizontal plates that support the core baffle plates) and by melting of some core former plates. Approximately 9 tons of molten corium was retained in the core bypass region. The primary evidence for this event is the analysis of the source range monitors.\(^3\) Since the relocation occurred with the core covered, a noncoolable geometry had formed within the core. Once the relocation occurred, it appears that the geometries within the core, core bypass, and lower plenum were coolable in that there is no evidence of another relocation, and only solidified material was found during plant defuelling. The observed core conditions at the end of the accident are shown in Figure 5.

This section has provided an overview of the accident events and phenomena important to performing and evaluating the results of computer code calculations for the four phases of the accident. Phase 1 of the accident is characterized by a short term loss of heat sink, and an SBLOCA. Modeling of the thermal-hydraulics of an SBLOCA loss of heat sink accident is sufficient for this phase. During Phase 2, the SBLOCA continued, and core heatup and melting occurred. The codes used for Phase 2 need to model both the thermal-hydraulics and the processes of fuel rod degradation. The models for fuel rod core degraded heatup need to consider the following phenomena:

- Cladding oxidation,
- Formation of low melting point eutectic phases,
- Clad ballooning and rupture,
- Clad melting, fuel dissolution, and candling of the molten materials,
- Formation of crusts due to freezing of prior molten material,
- Formation of molten pools within the core region, and
- Effects of geometry changes on the thermal-hydraulic solution.

However, it is recognized that severe accident codes do not model all aspects of the phenomena listed. During Phase 3, the code models needed to simulate the accident include:

- Reflood oxidation,
- Cladding embrittlement and shattering, and
- Heat transfer between the degraded core and coolant.
Figure 5. MI-2 Core End-State Configuration.
During Phase 4, the additional models required to simulate the accident is the failure of the supporting crust and relocation of molten materials to the lower plenum, and the heat transfer between the molten pool in the lower plenum, the coolant, and the reactor pressure vessel.

For a code to model all 4 phases of the TMI-2 accident, it must include models for the above phenomena. The models that represent the phenomena may provide various levels of fidelity depending on the purpose of the code. Codes such as SCDAP/RELAP5 attempt to model both the thermal-hydraulic and severe accident phenomena in detail, and therefore should be expected to produce results that are consistent with the detail provided in the models. On the other hand, codes such as MARCH have very simple models to represent accident thermal-hydraulics, and should not be expected to produce results to the same level of accuracy as the more detailed codes where thermal-hydraulics dominate the overall system response.

Not only are the sequence of events and models for the phenomenology required to simulate the accident, but also the boundary conditions. The boundary conditions used for the analysis exercise are discussed in the next section.

In summary the accident can be characterized for the purposes of analysis by four distinct phases. Phase 1 (0 to 100 min) is an SBLOCA with the RCS pumps on. Phase 2 (100 to 174 min) continues the SBLOCA with the RCS pumps off along with initial core heatup, melting, and in-core relocation. Phase 3 (174 to 200 min) is the 2B RCS pump transient short term cooling effects. Finally, Phase 4 is the reestablishment of safety injection and the relocation of ~30 metric tons of molten core materials to the core bypass and lower plenum a coolable geometry resulted from the relocation, and core degradation was terminated.
Boundary Conditions

As was mentioned in the previous sections the Analysis Exercise is driven by the boundary conditions, and not all of the boundary conditions were recorded at the time of the accident. The various codes used by the participants require differing specifications of the boundary conditions. However the conditions are specified they are derived from the same sources. The basic list of boundary conditions includes:

- Secondary side pressure and temperature,
- Secondary levels or AFW mass flow rate,
- Letdown mass flow rate, and
- HPI/make up mass flow rate.

In addition to the above boundary conditions, some of the codes (e.g., MARCH, SHAPE, MACRES, and SCDAP) require specification of the PORV mass flow rate. Of the listed boundary conditions, only the secondary side pressures, shown in Figure 6, were recorded. The remaining boundary conditions had to be inferred from other plant measurements and independent engineering analyses. The methodologies for estimating each of the boundary conditions are discussed briefly in the following paragraphs.

![Graph showing pressure changes over time](image)

**Figure 6.** Secondary side pressures.
While the AFW mass flow rate was not recorded the steam generator levels were measured with three different types and ranges of measurements start-up, operating, and full range (used for wet lay up) levels. Each measurement was based on a measurement of differential pressure within the steam generator. The three measurements of differential pressure were reconciled and put on a common basis of stratified liquid level referenced to the bottom tube sheet. The resulting levels for each steam generator are shown in Figure 7. In a number of the codes, such as SCDAP/RELAP5 and MAAP 3.0B, the AFW mass flow rate can be estimated via a control system, which calculates the AFW mass flow rate required to match the measured steam generator level. A similar estimation method is to first estimate the steaming rate of each steam generator during a period of decreasing level—during which it is assumed that the AFW rate is zero. Then assuming the steaming rate is constant use the derivative of the level during periods of increasing level less the steaming rate to estimate the AFW mass flow rate. This methodology was used by Anderson as an independent engineering estimate of the AFW mass flow rates. Anderson's estimate could be used as input to those codes not having a control system capability and requiring an estimate of the AFW mass flow rate as a boundary condition and checking the results of those codes using the control system method. Anderson's estimates of the AFW mass flow rates are shown in Figure 8.

The letdown mass flow rate was computed from the measured conditions on the secondary side of the letdown coolers and the primary side A-loop cold leg temperature. The letdown mass flowrate is shown in Figure 9. The uncertainty in the computed letdown flow is ± 1.2 kg/s.

![Graph showing steam generator levels](image)

**Figure 7.** Steam generator levels.
Figure 8. Estimated AFW mass flow rates (estimated based on calculated steaming rates).

Figure 9. Estimated letdown and HPI/makeup mass flow rates.
There is very little upon which to base an estimate of the HPI/makeup mass flow rate during the accident. Anderson\(^7\) estimated the makeup rate based on the following:

- Assume that HPI/makeup is piecewise constant,
- Base estimate on a mass balance,
- Estimate average HPI/makeup rate from 12 to core 100 min based on core void fraction estimate at 100 min from source range monitors, and
- Use the core uncover time to estimate the HPI/makeup rate from 100 to 174 min.

To obtain an estimate of the HPI/makeup flows during the accident using this method, a good estimate of the other RCS boundary flows is required. In addition to the letdown flow, the PORV mass flow rate is required. Nomura\(^8\) estimated the PORV flow based on the homogeneous equilibrium critical flow model at high quality, the Henry-Fauske critical flow model for all liquid, and a curve fit between the two models at low quality \((0 < x < 2\%)\). Nomura estimated the quality at the PORV orifice based on the measured pressurizer liquid level and the Wilson bubble rise model. The resulting PORV mass flow rate is shown in Figure 9. The uncertainty in the PORV flow is \(\pm 20\%\). At 100 min, this translates to \(\pm 20,800\) kg, or \(3.3\) kg/s on average during the first 100 minutes.

The HPI/makeup flows based on Anderson's mass balance analysis is shown in Figure 9, compared to the letdown mass flow rate. It is clear that the uncertainty in the HPI/makeup mass flow rate is large. In addition to the uncertainties in the letdown and PORV flows, there are the uncertainties associated with the core void fraction at 100 min and the time of core uncover plus the modeling uncertainty associated with assuming a piecewise constant instead of a possibly more realistic time variant makeup rate.

Due to the uncertainties in the RCS mass balance, the participants agreed to performing sensitivity studies with the makeup rate as the independent variable. The best estimate analyses coupled with a set of sensitivity analyses provides an analysis baseline that can yield meaningful conclusions regarding code performance. The principal boundary conditions required by each of the codes are listed in Table 2. The participants were provided a common data set in the initial and boundary conditions data base (ICBC)\(^9\). Data that were included in the ICBC were reviewed, and uncertainties in the data were determined. Individual components of the total uncertainty for specific measurement were combined conservatively by considering each component to be a bias, and adding the components using the root mean square method.\(^10\) The only boundary condition for which uncertainties could not be determined was the makeup mass flow rate. Because of the unquantifiable uncertainty in the makeup mass flow rate, we agreed that a common makeup rate, as presented above, would be used for a base case calculation, and parametric studies could be performed about the base case.
The initial conditions for the analysis are given in Table 3 as provided in the initial and boundary conditions database. Additional initial conditions at 100 min were provided (Table 4) for those participants desiring to start the analysis at the beginning of Phase 2. Analysis exercise participants were provided the initial conditions in the ICBC, with the uncertainties determined as described above.

**Table 2.** Boundary condition requirements.

<table>
<thead>
<tr>
<th>Code</th>
<th>Required Boundary Conditions</th>
</tr>
</thead>
<tbody>
<tr>
<td>CATHARE1/ICARE</td>
<td>HPI/makeup flowrate, letdown flowrate, steam generator pressure and levels</td>
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<tr>
<td>FLOW3D</td>
<td>Conditions at reactor vessel boundaries</td>
</tr>
<tr>
<td>MAAP</td>
<td>HPI/makeup flowrate, letdown flowrate, steam generator levels, and secondary side pressure</td>
</tr>
<tr>
<td>MARCH</td>
<td>HPI/makeup flowrate, letdown flowrate secondary pressure, and FORV flowrate</td>
</tr>
<tr>
<td>MELCOR</td>
<td>HPI/makeup flowrate, letdown flowrate, steam generator pressure and levels.(?????)</td>
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<tr>
<td>MELPROG</td>
<td>Conditions at reactor vessel boundaries</td>
</tr>
<tr>
<td>SCDAP</td>
<td>Conditions at reactor vessel boundaries</td>
</tr>
<tr>
<td>SCDAP/RELAP5</td>
<td>HPI/makeup and letdown flowrates, steam generator levels, and secondary pressure and temperature</td>
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<tr>
<td>SHAPE</td>
<td>Conditions at core inlet and outlet</td>
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<td>THALES</td>
<td>HPI/makeup, letdown, emergency feedwater flowrate</td>
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<td>TRAC (MELPROG/TRAC)</td>
<td>HPI/makeup and letdown flowrates, steam generator levels, and secondary pressure and temperature</td>
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<td>ATHLET</td>
<td>HPI/make-up, letdown, secondary pressures, and AFW flow</td>
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### Table 3. Initial conditions at turbine trip.

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<th>Condition</th>
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<th>Value</th>
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<tr>
<td>Emergency Feedwater Injection SG-B</td>
<td>AFW-SG-B</td>
<td>0.00E+00 kg/s</td>
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<tr>
<td>Cold Leg Temperature 1B</td>
<td>RC-5B-TE2</td>
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<td>Cold Leg Temperature 2A</td>
<td>RC-15A-TE3</td>
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<td>Cold Leg Temperature 2B</td>
<td>RC-15B-TE3</td>
<td>5.65E+02 K</td>
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<td>Hot Leg Temperature A-Loop</td>
<td>TE-HL-A</td>
<td>5.92E+02 K</td>
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<tr>
<td>Hot Leg Temperature B-Loop</td>
<td>TE-HL-B</td>
<td>5.92E+02 K</td>
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<tr>
<td>HPI/Makeup Base Case</td>
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<td>Letdown Mass Flow Rate</td>
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<td>Main Steam Temperature A</td>
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<td>Pressurizer Coolant Mass @ 100 Min</td>
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<td>RCS Coolant Pump 2B</td>
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<td>Total Pressurizer heater Power</td>
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Results and Code Comparison

As with the accident sequence, the results and code comparison are discussed for each phase of the accident. Each accident phase has a distinct set of controlling phenomena. The analysis exercise results are discussed in relation to the controlling phenomena and the available data to which the results can be compared. However, the lack of recorded data for the accident and the limitations imposed by the uncertainties in the boundary conditions means that the comparison is to a large degree an inter-code comparison. It is useful to remember that the makeup flow was estimated based on the estimated time of initial core heat up and minimum core water level, and is an average value over the various time spans for which the makeup flow was estimated.

Phase 1

As previously stated, RCS pressure is controlled by secondary side conditions (in particular, OTSG pressure) while the RCS coolant is at saturation. The second parameter needed to fix RCS state is the void fraction. However, the void fraction is determined by the boundary flows. All but the PORV flow were specified by the analysis exercise (some codes such as MARCH require specification of the PORV flow). Since there was no direct measurement of the PORV flow during the accident, there is no plant measurement to compare the code calculations to other than the estimate provided by Nomura's analysis (see the previous section). One of the challenges to codes and analysts was the calculation of the behavior of the once through steam generators. Creative approaches such as time-varying control volumes or feedwater injection points were used by some analysts to provide improved matches of OTSG pressure and levels, so that the proper boundary conditions for the primary system could be established. The simpler codes used OTSG pressure or the estimated feedwater flow rate as input. RCS pressure is the only calculated result compared for Phase 1. The results of the participant's calculations are compared to the recorded RCS pressure in Figure 10. In general, the code calculations can be divided by the thermal hydraulic sophistication of the codes. The results of the thermal hydraulic oriented codes (CATHARE for example) are closer to the data than those of the simpler severe accident oriented codes (e.g., MARCH). For the simplest codes, the specification of additional boundary conditions (e.g., PORV flow), generated by more sophisticated hydraulic models, assisted in generating the acceptable results for Phase 1.

Phase 2

For Phase 2 of the accident, RCS pressure, pressurizer level, and hot leg temperature were compared. As with Phase 1, RCS pressure provides an overall indicator of the system state. A comparison of calculated and measured RCS pressure for Phase 2 is included in Figure 10. The code calculations generally agree with the data and are consistent with each other up to the start of the pressure increase at about 125 min.

At about 125 min, which is the onset of the severe core damage phase of the accident, the calculated pressures begin to diverge from the data and from each other. For example, the base-case INEL SCDAP/RELAP5 calculation
Figure 10. Comparison of RCS pressure calculations.
Figure 10. (continued).
shows a slow decrease in pressure after closure of the PORV block valve at 139 min, while the measured pressure begins to rise. This divergence in pressure results from the calculated draining of the pressurizer after block valve closure, in contrast to the observed hold-up of liquid in the pressurizer. A parametric calculation with SCDAP/RELAP5, in which the makeup flow rate was reduced after 100 min, produced a calculated pressure closer to the data and no draining of the pressurizer. This demonstrates the sensitivity of the calculations to the estimated boundary conditions.

Other codes had similar difficulty in reproducing the observed holdup of liquid in the pressurizer. This is evident from the comparison of pressurizer level shown in Figure 11. As with RCS pressure, there is divergence in the results. Once the PORV block valve is closed, pressurizer level becomes constant, and the pressure in the steam dome of the pressurizer remains approximately 0.05 MPa less than the hot leg pressure. As noted in the discussion of RCS pressure, SCDAP/RELAP5 is unable to reproduce these results with the boundary conditions as specified, but approximates the data for the parametric case of reduced makeup flow rate after 100 min. Anderson,11 in his analysis of the steam generator levels, concluded that the level response from 100 min to block valve closure depended on counter-current flow limitations at the pressurizer surge line junction. The limitations of counter-current flow modeling in RELAP/MOD2 may be at least a partial cause of SCDAP/RELAP5’s inability to replicate the observed hold-up of liquid in the pressurizer for the base case boundary conditions. Other reviewers concluded that counter-current flow difficulties should not be a crucial limitation for the TMI-2 case, as the pressurizer surge line dips about 3 m below the hot leg in the TMI-2 piping layout. Similar difficulties were encountered by most analysts. CATHARE, for example, produced the desired result only after the analyst inserted a valve in the surge line and closed it before drainage could occur.

An interesting variability of calculated results is shown by the MAAP-3B calculations performed by FAI and VTT. Pressurizer drainage occurs in the VTT calculations, and does not occur in the FAI calculations. It has long been recognized that not only code capabilities, but the use of the codes by different analysts can result in variations of the output, although such variations are usually expected to be small. For this phase of the accident, however, small variations in boundary conditions, and apparently, in calculated results can have a major impact on the predicted accident progression (i.e., so-called “cliff-edge” effects).

It is apparent that the onset of conditions beyond the design basis during this phase of the accident presents a severe challenge to the codes, as well as the analysts. It is recognized that the uncertainties in boundary conditions for TMI-2 exacerbate the difficulty of this challenge. Nevertheless, the observation that reasonable small variations in boundary conditions, similar but not identical code versions, or different analysts, can result in strongly diverging results leads to the conclusion that further work is required before code predictions can be considered reliable without parametric studies to search for such “cliff-edge” effects.
Figure 11. Comparison of calculated pressurizer levels.
These observations are reinforced by the results shown in Figures 12 and 13. These figures depict the core heatup and hydrogen generation predicted by various codes. Although no accurate observation is available for comparison with the calculated results, the divergence of results obtained by the different codes, and by users of the same code, indicates that the status of the codes cannot be considered mature.

Because no record of in-core temperatures exist for core heatup, indirect estimates of core heatup are required to evaluate predictions of core heatup. A global indicator of core heatup is the estimated hydrogen generated by cladding oxidation. The hydrogen generated during the accident was not directly measured, but has been estimated\textsuperscript{1-12} based on the in-containment hydrogen burn, energy balances for the 2B RCS pump transient, steam generator partial pressures, and end-state observations of the reactor core. These methods are based on the available accident data and are independent of the code calculations, which model the oxidation process. Examination of hydrogen generation estimates during core heatup indicate at least a part of the source of the spread in the RCS pressure estimates. First the estimates of total hydrogen generated during Phase 2, as shown in Table 5, are spread by a factor of \textasciitilde3. Further the time history of the various estimates is quite different (see Figure 12). The time at which oxidation commences is generally the same and consistent with the initiation of RCS repressurization. However, once oxidation has started the estimates spread and even the shapes of the hydrogen generation curves are different. Thus, both the magnitude and trend are not in agreement. It is difficult to draw any firm conclusions as to the causes of the differences. However, possible causes may include inaccurate models for cladding dissolution, melting, relocation, and oxidation. Another aspect of the estimated hydrogen generation is that a number of the codes (SCDAP/RELAP5, MARCH/CTN, MARCH/BCD, & MAAP/DOE) predict cessation of oxidation prior to 174 min. This implies that metallic Zr has been completely oxidized within or relocated away from the high temperature region of the core. This appears to have a significant impact on the predictions for Phase 3 as discussed below.

Another aspect of Phase 2 is the potential for in-vessel natural circulation. The UKAEA MELPROG and FLOW3D calculations attempt to assess the gas flows during Phase 2 of the accident. A natural circulation path with the flow upward at the center of the core and downward at the core periphery is predicted with a complex upper plenum flow. The upward flow is broadly consistent with observed upper grid damage. The reference MELPROG calculation, prior to code failure at 158 min, predicts the maximum temperature of the upper grid plate to be \textasciitilde1450 K. Visual observations\textsuperscript{10} of the lower surface of the upper grid plate indicate that some melting of the lower surface occurred (although not in the center), indicating that peak temperatures of the grid plate reached at least 1700 K. Based on the accident scenario, the best estimate time of damage to the upper grid plate is during the 2-B RCS pump transient, as discussed for phase 3. Although the peak upper grid plate temperature, the hydrogen that was generated, and the temperatures are low, relative to 174 min, the calculations are not any more inconsistent with the accident than the other calculations. As shown in the UKAEA report, the FLOW3D results are similar to the MELPROG results.
Figure 12. Comparison of calculated core cladding temperatures at core top center.
Figure 13. Estimated hydrogen generation.
Figure 13. (continued).

Table 5. Hydrogen generation during the TMI-2 accident.

<table>
<thead>
<tr>
<th>Code/Method</th>
<th>Phase 2</th>
<th>Phase 3</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Hydrogen generated prior to 2B RCS pump transient, $\leq$ 174 min (kg)</td>
<td>Hydrogen generated after 174 min (kg)</td>
</tr>
<tr>
<td>Estimates from accident data</td>
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<td>160</td>
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<tr>
<td>ICARE</td>
<td>145</td>
<td></td>
</tr>
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<td>MAAP/DOE</td>
<td>409</td>
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<td>30</td>
</tr>
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<td>MARCH/CTN</td>
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<td>MARCH/ECN</td>
<td>471 (TOTAL)</td>
<td>(?????)</td>
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<td>MELCOR</td>
<td>225 (TOTAL)</td>
<td>(?????)</td>
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<td>MELPROG</td>
<td>(?????)</td>
<td>(?????)</td>
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<td>SCDAP/RELAP5</td>
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<td>0</td>
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<td>SHAPE</td>
<td>165</td>
<td>&lt;10</td>
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<tr>
<td>THALES-PM1/TMI</td>
<td>$&gt;$ 400</td>
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Fission product release and transport calculations were performed by JINS using the SHAPE and MACRES code suite. The release calculations estimate 71% of the core inventory of cesium and iodine, ~3% of the ruthenium and ~6% of the strontium were released from the fuel. Estimates of the retention of the high volatile fission products, cesium and iodine, in the damaged core materials\textsuperscript{13} yield a release of ~60% from the core. Estimates for the medium volatile fission products, ruthenium and strontium, are ~9% and ~8% respectively. Considering the potentially large uncertainties in the retention estimates, the calculated releases are relatively close to the observations.

In summary the calculations diverge at the initiation of core heatup, with significant differences in the estimates for RCS pressure and hydrogen generation.

**Phase 3**

The primary feature of Phase 3 is the 2B RCS pump transient. When the pump was started at 174 min 30 m\textsuperscript{3} (1000 ft\textsuperscript{3}) of water was injected into the reactor pressure vessel. It is believed that significant relocation of molten core materials had relocated from the upper region of the core downward to form a predominantly metallic crust at about 0.7 m (2 ft) from the bottom of the core. This crust formed a crucible that collected other downward-flowing molten core materials. When the pump transient pumped water into the vessel and core the extensive cooling occurred freezing the outer crucible geometry. Metallic zirconium remaining in the upper region of the core was provided with a source of oxygen for oxidation to occur. Kuan\textsuperscript{2} estimated that ~160 kg of hydrogen were generated due to cladding oxidation during the pump transient. None of the codes predict hydrogen generation anywhere close to 160 kg during the pump transient. As noted above none of the calculations estimate hydrogen generation of any significance during the pump transient. Any predictions of continued pressurization during the pump transient are a result of steaming in the core without heat removal by the secondaries. This is inconsistent with the best estimate accident scenario. One plausible cause for the calculated behavior is the lack of a model specific to reflood oxidation. None of the codes used for the analysis exercise have models that specifically consider reflood oxidation. Another possible explanation is inaccuracies in the relocation models for core materials. Models for relocation may be over estimating the relocation of metallics to the lower core region where temperatures are below the limit for rapid oxidation.

Another attribute of the pump transient is that the codes that predict a pressure increase during the transient without significant oxidation are also failing to predict the heat transfer to the B-loop OTSG during the initial phase of the pump transient. Kuan estimated that The B-loop main steam isolation valve (MSIV) was closed at the initiation of the pump transient. The pressure rise (see Figure 1) in the B-loop OTSG during the pump transient is a direct result of the energy transfer from the RCS to the secondary side. Kuan\textsuperscript{2} estimated that 15 GJ of energy were transferred from the primary to secondary during the pump transient as a result of steaming in the core. The codes do not appear to be transferring nearly this quantity of energy during the pump transient.
Phase 4

During Phase 4, approximately 29 tons of molten core material relocated to the core bypass region and the lower plenum. It is believed that the pump transient had the effect of freezing in place the geometry in the central region of the core as shown in Figure 4. Moor estimated that at least 7 tons of molten corium had to be contained in the crucible at 174 min to have at least 29 tons of molten corium at 224 min. The few calculations (for example MARCH/CTN, MAAP/DOE, and SCDAP/RELAP5) that attempt to model the relocation report that essentially insufficient molten material to exist at 224 min as the result of insufficient molten material contained in the crucible after the pump transient. The PLUGM calculations of melt relocation through an intact fuel assembly were inconclusive, the results being dependent on the assumed mode of freezing. It is now known that melt did not relocate by this route. None of the codes use have demonstrated the capability of calculating the relocation through the bypass region. Even if the codes had entirely adequate models for Phase 4, there is little chance of simulating the core relocation accurately without first simulating Phases 2 and 3 accurately. Another view of the process is that the calculations must proceed through distinct windows at the beginning and end of Phases 2 and 3 for Phase 4 to be adequately simulated.
Conclusions

The analysis exercise provided a unique opportunity to test the capabilities of severe accident codes against an accident involving severe core damage.

In contrast to a "blind" standard problem code comparison, this exercise not only provided the "answers" up front, but encouraged the modelers to select, by trial and error, if necessary, all adjustable parameters, and use repeated calculations in order to elicit the best possible match between code and actual observations. Another key difference from a standard problem is that, although the end-state configuration is known, certain boundary conditions, needed as input to the codes, were not known precisely, but had to be estimated, resulting in considerable uncertainty for parameters not measured by the plant instrumentation.

The results of this effort provide a valuable insight into the capabilities of today's severe accident codes. The match to the TMI-2 observations achieved by the various codes and analysts varied significantly for the four phases of the accident, as described below:

Phase 1

Simulation of Phase 1 requires only mass and energy balance calculations to produce adequate estimates of RCS pressure and initial conditions for Phase 2. All participants were able to end Phase 1 with reasonable estimates of the initial conditions (mass inventory and RCS pressure) for the initiation of Phase 2. Even codes that represent the primary system in a relatively simple manner (small number of volumes to represent the RCS an/or no model for the RCS pumps) demonstrated the capability to estimate the primary to secondary energy transfer. However, it should be remembered that Phase 1 of the accident is a relatively simple thermal-hydraulic transient.

Phase 2

The initial segment of Phase 2, up to the time at which repressurization of the RCS started, is adequately estimated by the participants with respect to RCS pressure. After the start of core heatup and degradation, the codes diverge on their estimates of RCS pressure and hydrogen generation. Although the participants used the same boundary conditions for the base case, and adjusted the boundary conditions for sensitivity studies, they were unable to calculate a consistent system response. It is concluded that improvements in modeling techniques are required before the codes can reliably predict accident progression beyond the well-established design basis range. Another example is the estimated pressurizer level. Complete drainage of the pressurizer was over estimated by several codes after the PORV block valve was closed for the base case, while the drainage was reduced by decreasing the makeup rate. Although the specified boundary conditions may be uncertain, parametric calculations indicate the sensitivity of the estimated pressurizer level to the conditions at the time of block valve closure and the need for accuracy in the

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calculations. Inaccurate calculations lead to a prediction of core quenching by the liquid draining from the pressurizer to the core, and a prediction that the accident would be far less severe. For pressurized water reactors, the quantity of liquid in the pressurizer could, therefore, be significant to accident progression. We conclude that for accidents that have potential delayed liquid drainage from the pressurizer, detailed modeling of the pressurizer is required to reasonably predict the course of the accident.

Phase 3

None of the calculations presented herein predicted significant hydrogen generation during the pump transient. Any prediction of RCS repressurization during the pump transient occurred in the codes as a result of steam production in the core. We conclude that the code models are deficient in describing one or more of the following phenomena: reflood oxidation, relocation of zircaloy prior to the pump transient, and spatial distribution of oxidation during core heatup.

Phase 4

There are an insufficient number of participant calculations of Phase 4 to allow comparison between codes. However, it is noted that no code has demonstrated at this time a capability to model the phenomena associated with the formation of crusts, the retention of liquid corium in the core region, and the failure of the corium crucible at the periphery of the core.

These results demonstrate that the relatively good capabilities (achieved in several codes with quite simple modeling of the primary system) for the early phases of the accident rapidly diminish as the accident progresses beyond the design basis conditions. Models for cladding oxidation, formation of low temperature eutectics, structure and geometry of the degraded core, core debris heat transfer, and materials interaction during the later states of the accident are either rudimentary or have not yet been incorporated into the codes compared in this exercise.

This outcome was not unexpected since a basic understanding of the phenomena involved in the later stages of a core melt accident, and in quenching a degraded core, has evolved only recently, as a result of the data from TMI-2, LOFT, and several in-pile and out-of-pile experiments. It should be noted that several of the codes used have evolved even during the conduct of this exercise. Code improvements have resulted from improved understanding of severe accident behavior gained from the exercise itself, from recent experiments, and the TMI-2 debris examination. This experience leads to the expectation that significant progress in the severe accident code capabilities can be achieved in the near future.
References


Appendix A

United Kingdom Atomic Energy Authority
THERMAL-HYDRAULIC CALCULATIONS IN TMI-2 ACCIDENT ANALYSIS

J N LILLINGTON
A J LYONS
I M LOVELY

REACTOR SYSTEMS ANALYSIS DIVISION

AEE WINFRITH
JANUARY 1989
SUMMARY

This paper describes Winfrith calculations of the TMI-2 initial heat-up phase submitted as part of the UK contribution to the CSNI TMI-2 Analysis Programme. The calculations cover the period between ~100 mins and ~174 mins from the start of the accident. During this time the primary loop pumps were stationary until B loop pump was restarted at 174 mins.

The main purpose of the work was to study the heat-up phase using the integrated mechanistic melt-progression code MELPROG, developed by Sandia National Laboratories, USA and also to investigate the nature of the gas (steam and hydrogen) flow over the uncovered core and upper plenum using the separate effects fluid dynamics code FLOW3D, developed at Harwell. Some FLOW3D calculations simulating the first few seconds of the B loop pump are also presented.

MELPROG results show that the vessel initial heat-up phase is sensitive to the uncertainty in the high pressure injection make-up flow during this period. Natural circulation patterns are shown to exist once the core is uncovered.

The most likely explanation offered to explain the lack of damage to the upper plenum is the timely operation of the B-loop pump, but the results are not conclusive.

The MELPROG calculations do imply the upper plenum structure temperatures are below those at which melting is significant, up to the time of initial clad melting and relocation.

Two-dimensional and spatially averaged three-dimensional FLOW3D calculations are found to be in broad agreement with corresponding MELPROG calculations of the gas flow for similar best estimate data. The recirculation rates and overall heat-up predictions by the two codes are similar.

Via FLOW3D sensitivity calculations and in the absence of ballooning, it is shown that uncertainties in the flow resistance imply uncertainties of up to 100 K in the core/upper plenum structure temperature differences.

FLOW3D calculations representing the detailed geometry of the upper plenum, are shown to predict three-dimensional natural circulation flow paths and upper plenum temperature profiles which are consistent with the observed 3-D damage of the upper grid plate. The 3-D effect is found to be more pronounced in the event of a large coherent core-blockage.

The effects of ballooning on natural circulation and on the relative core/upper plenum heat-up are only shown to be significant if there is substantial blockage over the whole core (~ 90% flow area blockage). The effects are much reduced if a bypass exists.

AEEW-R 2434
Three-dimensional FLOW3D simulations of the B-loop pump transient are presented which show that the flow did not bifurcate below the level of the grid plate and that the flow was essentially two-dimensional at that level. The implementation is that the observed bi-polar nature of the damage on the grid plate could only have occurred during the pump transient by virtue of a pre-existing temperature distribution prior to the pump transient (and by the above most probably induced by natural circulation).

Fine mesh calculations using FLOW3D are presented for both the buoyancy driven flow conditions prior to and the forced flow conditions during the B-loop pump transient, in order to assess the adequacy of the coarse mesh calculations of the integrated codes, and in particular MELPROG. It is shown that there is mesh dependency in the thermal-hydraulics results themselves although this is probably not significant compared with the effects of mesh on melt progression and other uncertainties, issues which are beyond the scope of this paper.
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1 INTRODUCTION

This report describes work carried out at Winfrith on the early phase of the TMI-2 accident as part of UK participation in the CSNI TMI-2 Analysis Programme. Other theoretical analysis is ongoing at Culham. Core sample analysis is being carried out at Windscale, as part of the complimentary CSNI Sample Analysis Programme. The UK is also participating in the CSNI/NRC lower head examination scheduled for mid 1989. The main objectives of the overall UK study are:

1. To obtain a consistent, qualitative and as far as possible, quantitative understanding of the course and main physical features of the accident up to major core relocation.

2. To demonstrate and, if appropriate, develop a capability to calculate the most important features of the accident.

3. To assess the implications for other possible accident scenarios relevant to future PWRs in the UK.

2 ISSUES ADDRESSED IN THE PAPER

The main thrust of the Winfrith work reported here is an investigation of the gas flows present in TMI-2 during the initial heat-up phase. One of the most interesting observations is that the upper vessel structures were largely undamaged, apart from some limited oxidation to the upper grid plate, despite very high core temperatures during this period. This is contrary to expectations from many code predictions for hypothetical unrecovered transients which imply significant damage to the upper vessel and failure of the hot leg/surge line through overheating via natural circulation. An important question to be addressed in this study is therefore:

(a) Why did the TMI-2 upper plenum remain relatively cool?

Calculations have been carried out with the mechanistic core degradation/thermal hydraulics code MELPROG [1] developed by Sandia National Laboratories, USA. However, certain issues relating to natural circulation in severe accidents, and in particular TMI-2, are beyond the scope of MELPROG. Particular issues concerning in-vessel recirculation in TMI-2 addressed here are:

(b) Are there details in the geometry that are important but cannot be represented adequately by MELPROG?

(c) Are 3-D effects important (MELPROG is a 2-D code)?
The initial heat-up phase was terminated by the B loop pump transient and another question considered is:

(d) What were the thermal-hydraulic effects of the B-loop pump quench?

Finally the effects of mesh refinement are investigated asking:

(e) Are the coarse mesh analyses using MELPROG and other integral codes adequate?

To investigate these questions (b), (c), (d), (e) fully, calculations using a 3-D fine scale thermal-hydraulics code FLOW3D [2] developed by Harwell are performed.

To some measure, the effects of mesh refinement could be investigated within the thermal-hydraulics module in MELPROG, except that the computing time required would be very expensive. Moreover azimuthal effects could not be considered owing to the 2-D nature of the code.

For convenience of analysis, the TMI-2 accident has been partitioned into 4 phases:

Phase 1 (0-100 min) - a purely thermal-hydraulic phase involving loss of primary circuit water inventory.

Phase 2 (100-174 min) - the initial heat-up phase during which the initial melt progression and blockage formation is thought to have occurred.

Phase 3 (174-200 min) - period of the B-loop pump operation and continued blockage heat-up.

Phase 4 (200-300 min) - HPI injection and melt relocation to the lower plenum.

This report concentrates mainly on the events during Phase 2, with some limited analysis of the B-loop pump transient in Phase 3.

3 MELPROG CALCULATIONS

3.1 Modelling Assumptions

The MELPROG vessel representation is shown in Figure 1. The dimensions have been taken from the Standard Problem Package data published by EG&G [3]. Most of the main structures in the vessel are included, in particular the lower core support structures, the upper grid assembly, the core barrel and the outer vessel thermal shield. It should be noted that the model is azimuthally symmetric as required by the code.

A difficulty in setting up the MELPROG representation
was in attributing appropriate flow resistance for the various in-vessel structures eg flow distribution plates etc. In the calculations described in this paper, values have been chosen to be consistent with losses for structures in simple geometries, tabulated in the literature.

Considerable work has been carried out at Los Alamos towards developing a primary circuit TRAC representation for TMI-2. By comparing converged MELPROG/TRAC results for steady-state nominal flow conditions, with plant data, it has been possible to determine whether the overall resistance in the vessel is about right (at least under these conditions).

Considerable care has been taken to ensure that the water/steam volumes in the core, plenum etc are correct and that the masses of the various plant components are also correct in the MELPROG representation.

The analysis presented here for the heat-up phase has been carried out using the stand-alone version of MELPROG with supplied boundary conditions on the hot and cold leg boundaries. The pressure at the hot leg was adjusted to the primary circuit pressure measured during the accident [3]. Inlet flow and enthalpy conditions for the cold leg boundary were based on various recommendations from EG&G, as defined below.

3.2 Discussion of Results

3.2.1 Reference Calculation

The reference calculation was carried out with 4 kg/s constant injection make-up flow rate as recommended by EG&G [4], on the basis of a mass balance analysis.

The conditions for the start of the Phase 2 calculations were chosen to ensure that the top of the core begins to uncover at about 6500 s (108 min).

Figure 2 shows that the collapsed liquid level falls to ~1.1 m above the bottom of the active core, thereafter levelling out. At this point the water evaporated by the fraction of the decay heat dissipated by evaporation exactly balances the make-up flow rate of 4 kg/s.

Figures 3-6 show the cladding temperature profiles in the core. By ~8200 s (137 min) the peak rod temperatures on the centre-line exceed 1100 K, which is about the threshold temperature for ballooning and burst under the conditions of the accident. Ballooning, however is not modelled in these MELPROG calculations.

As the core uncovers, natural circulation occurs between the core and upper head. The flow is upwards at the
core centre and downwards at the core periphery. It develops as a consequence of the decay heat radial profile, heat losses at the core periphery and the circulation tends to increase as the temperatures increase due to preferential oxidation heating at the core centre. Natural circulation is discussed in more detail later, in the context of the FLOW3D predictions. The magnitude of the recirculating velocity predicted by MELPROG is in the range 0.5 - 1 m/s.

One effect of natural circulation is to produce more uniform temperatures axially in the recirculation zone above the water level in the core region in comparison with one-dimensional flow cooling. Only small axial temperature gradients are seen where the flow is upward, Figures 3 and 4 and where the flow is downward at the top of the core periphery, Figure 5. Near the bottom of the core periphery the steaming flow and the downward flow in the recirculating region oppose each other giving rise to increased axial temperature variation, Figure 5.

At 158 mins, relocation of the control rod material at ~ 1700 K occurred and it was not possible to carry the calculation further. This is shown for example in the instability on the fuel rod axial temperature in Ring 1 at this time, Figure 3.

Figures 6, 7 and 8 show the temperatures of upper support plate, plenum cylinder and upper grid plate. By 158 mins, Table 1, the peak temperatures of these three components are 840 K, 1400 K, and 1450 K respectively.

Due to the hot upward flow of steam the centre grid plate temperatures are always higher than at the periphery, Figure 8. This is typical of in-vessel 2D natural circulation calculations. The core gas temperatures tend to follow the fuel rod temperatures to within a few tens of degrees in the core region, Figures 3, 4, 5 and 9. The temperature differences between the gas and the upper plenum components are of the order of several hundreds of degrees cf Figures 6, 7, 8 and 10, 11, 12. Figures 10, 11, 12 show that there is little temperature variation either axially or radially within the gas in the upper plenum.

Figures 13 and 14 show the hydrogen generation rate and cumulative generation. The total generation rate did not exceed ~ 0.07 kg/s with ~ 70 kg of hydrogen total by the time of initial control rod material relocation. Figure 15 shows the percentage oxidation of the clad as a function of elevation in Ring 1.

Increased hydrogen production as shown in Figures 13-15 during the material relocation is expected physically, but the actual peak values of hydrogen predicted
160 kg may well be an artifact of the numerics. It was not possible to overcome the numerical problems and continue the calculation further even with stringent timestep reduction.

3.2.2 Effect of Make-up Flow Rate Variation

There is some evidence, based on operator interviews, that coolant injection may have started at a higher rate and then decreased later in the 100–174 min period [4]. A second make-up flow profile was therefore recommended by EGaG: 8 kg/s for 100–137 min and zero flow between 137 and 174 mins. This gives the same cumulative flow over the period as that in the reference calculation.

The assumed initial conditions were taken to be the same as for the reference calculation. In comparison with the reference calculation the downcomer and vessel liquid level in this calculation continues to fall due to zero make-up flow, Figure 16. Figures 17, 18, 19 show that by 10200 s (170 min) the nodes at the 2.54 m elevation are now uncovered.

Figures 20, 21, 22 show the temperatures of the upper support plate, plenum cylinder, and upper grid plate to be 840 K, 1300 K, 1370 K, slightly lower but quite similar to the corresponding structures temperatures in the reference calculation at the time of initial material relocation. The gas temperatures Figures 23, 24, 25, 26 follow the structure temperatures in a similar manner to that described for the reference calculation.

Figures 27, 28, 29 show that the hydrogen generation rate, the cumulative hydrogen production and the fraction of Zr oxidised are similar to (just slightly reduced) the corresponding results for the reference calculation at the time of initial melt relocation.

In summary, therefore, the main effect of the increased make-up flow during the early half of Phase 2, at the expense of zero make-up flow in the latter half, is to delay the time to initial material relocation.

3.3 Comparison with TMI-2 Measured Data

Table 1 attempts to compare the predictions of the reference and sensitivity calculations with the evidence on the vessel heat-up phase that is available from measurements taken during the accident [5], [6].

During the accident, the response from containment radiation monitors indicated that the rods stated to balloon and burst from about 137 mins, implying peak cladding temperatures ~1100 K at that time. The predictions from both the reference and sensitivity

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Calculations are broadly consistent with this phenomena, showing peak centre-line cladding temperatures in the range 1050–1150 K during this time.

Estimates have been made of the vessel coolant level during the heat-up phase [6] but there are large uncertainties from the point of view of underpinning code calculations. However the implication is that the water level remains too high at the end of the heat-up phase in the MELPROG reference calculation.

The in-core self-powered neutron detectors (SPNDs) provide important data on the core heat-up. These data indicate instrument temperatures greater than 1350 K by about 150 min, near the top of the core. These would seem to be consistent with the results from the reference calculation, Table 2, in which centre-line temperatures > 1500 K are predicted, with the indication that the heat-up predicted in the sensitivity calculation is rather too low at this time.

Also at this time the system pressure started to increase significantly, which may be attributed in part to the start of hydrogen production and consequent degradation of heat transfer in the steam generators. The reference calculation shows hydrogen production starting to increase significantly from about 150 min. The sensitivity calculation shows rather less hydrogen produced by this time.

At 9800 s (165 min), the in-core SPND data indicate temperatures in excess of 1350 K at the lower core elevations, down to about 0.76 m from the bottom of the active fuel. By this time the reference calculation results show too low temperatures at these elevations. The sensitivity calculation results are broadly consistent with the SPND data showing temperatures at elevation 3.15 m (1.2 m from the bottom of the active fuel) were climbing rapidly. Similarly by this time, the core liquid level is much lower in the sensitivity calculation than in the reference calculation, and closer to the end state level ~ 0.6 m from the bottom of the active fuel.

Concerning the damage to the upper grid plate, it would seem from the defuelling programme that parts of the underside reached temperatures in excess of 1700 K. The upper surface was relatively undamaged. The sensitivity calculations show peak plate temperatures of ~ 1370 K by ~ 170 min. Thus upper plenum temperatures would appear to be underpredicted in the sensitivity calculation. The reference calculation shows peak temperatures of 1450 K by 159 min. Extrapolating to 174 min, without assuming material relocation would imply plate melting near the centre-line by some margin. However, material relocation would be expected to reduce the rate of
temperature increase in the upper core and plenum, and therefore the reference calculation results would not seem that unreasonable.

These two calculations demonstrate that the make-up flow rate uncertainties are important in determining the actual timescales of the temperature excursion. There is also insufficient plant data to underpin the calculations satisfactorily. The indications from these calculations are that the reference calculation make-up flow is too high at the end of the heat-up phase. The sensitivity figure is too high at the beginning but more in accordance with the data at the end of the heat-up phase. Thus overall these two calculations would seem to provide some reasonable bound for the initial core heat-up rate.

4 FLOW3D CALCULATIONS

4.1 Modelling Assumptions

FLOW3D calculations have been carried out to examine the behaviour of the gas phase and the heat-up of the core, upper plenum etc., after the time that the water level is low in the core. As its name suggests, FLOW3D has the capability to solve the complete 3-D equations of fluid dynamics. Equations for temperature and passive scalars are also included, enabling convective and radiative heat transfer, Zircaloy oxidation and hydrogen transport models to be straightforwardly included. A range of solution algorithms are available for solving the coupled set of equations.

In the first release version of FLOW3D used here, buoyant flows are predicted using the Boussinesq approximation for the variation of density with temperature. Regions of fine structure are treated as porous regions with an enhanced resistance to flow.

The water level is a required input into the FLOW3D calculations, and a representative figure was taken from the MELPROG reference calculation. This shows that the water level levelled out to about 1 m above the bottom of the core. FLOW3D calculations were carried out assuming this was the lower boundary of the calculational domain.

The domain of interest is shown in Figure 30.

Region 1 - Heated Core Region

For this region the flow area was estimated from Reference [3] data and the porosity of 0.55 defined accordingly to represent the presence of fuel and control rods. The pressure drop loss coefficient was set to that appropriate for a turbulent axial flow.
through rod bundles. This was varied as a sensitivity parameter.

Two temperature fields were solved for the core, one for the fluid and one for the solid. The temperature of the fuel pins and other core components were computed as the solid temperature with decay and oxidation heat generation and heat exchanges to the surrounding fluid were included in the model.

**Region 2 - Unheated Core Region**

In this region immediately above the active core there is no decay heat. The porosity was set to be the same as that in the core region.

**Region 3 - Guide Tube Assembly Outlet Nozzle**

This was modelled as in Region 2.

**Region 4 - Upper Plenum**

The upper plenum guide tubes occupy this region. A porosity of 0.9 was estimated based on data from [3].

**Region 5 - Plenum Cylinder with Flow Holes**

This was modelled as a frictionless wall except in the hole regions around the periphery of the upper plenum, Figure 30. Perforated plates containing small flow holes are located opposite the hot leg nozzles, see for example Figure 31.

**Region 6 - Hot Leg Nozzle**

This was represented by an annulus of open space, as shown in Figure 30.

**Region 7 - Core Support Plate Flange Region**

This is described below, see Region 8.

**Region 8 - Plenum Cover Plate**

The flow resistance in the regions 7 and 8 has been modelled by assuming that losses are similar to those of an orifice plate. A small porosity of 0.1 was used.

**Region 9 - Upper Head**

This is mainly open space. It was assumed that the conducting solid representing the top surface has the properties of stainless steel.
Region 10 - Holes in Plenum Cylinder

The presence of large exits at the top of the plenum cylinder together with exits opposite the hot legs are complicating features of the TMI-2 upper plenum.

The FLOW3D representation of the large holes is shown in Figures 32 and 33.

Region 11 - Core Bypass

This was treated as a stagnant region with appropriate fluid properties.

Figures 32 and 33 show the exact elevations of these regions.

4.2 Two-Dimensional FLOW3D Calculations

Figure 32 also shows the axial and radial mesh used in FLOW3D representation. The plenum cylinder is two-dimensionalised with flow paths between radial nodes 4 and 5 at axial nodes 8 and 10, as shown in Figure 32.

4.2.1 FLOW3D Calculation - MELPROG/FLOW3D Close

Comparison

The main objective in performing this calculation was to compare the FLOW3D results with MELPROG results for similar data. This also enabled some assessment to be made of the models in the two codes. The FLOW3D calculations were limited by the assumption of incompressible flow and also by the Boussinesq approximation. Therefore exact agreement with the MELPROG results would not be expected but nevertheless a reasonable measure of agreement in the recirculating flow rate, core plenum temperature differences and overall behaviour of core heat-up would be expected.

Figures 34-37 show a set of results for the MELPROG/FLOW3D close comparison calculation. The numbers in Figures 34 refer to the axial cell numbers shown in Figure 32. The results are characterised by an initial phase ~ 600s (10 mins) during which the results are influenced by the initial boundary condition. Thereafter the temperature rise rate between the various components of the system, core, upper plenum etc is essentially constant and only dependent on the heat input and total mass of the system. The relative temperature difference between the various components will depend on the flow recirculation rate. This will be small for high recirculation rates and high for low rates.

Table 2 shows a comparison between results from MELPROG and FLOW3D when the peak core temperatures are ~ 1400 K,
corresponding to a time ~ 150 min into the accident. This corresponds to a time almost 20 mins from the start of the FLOW3D calculation and is therefore well past the time of influence of the assumed initial boundary condition.

Comparing the recirculation rates predicted by the two codes in the upper plenum shows that the values agree to around 20%. FLOW3D predictions though are considerably higher in the core region.

Both codes show core fuel rod/gas temperature differences of a few tens of degrees and mean plenum structure/gas temperature differences of a few hundreds of degrees.

The MELPROG results show the peak core/upper grid plate temperatures differences ~ 400 K, with a similar figure for the core plenum cylinder difference rising to about 600 K for the core/support plate temperature differences. In the FLOW3D results the upper grid temperatures are hotter than in the MELPROG predictions, presumably because of the increased upflow in the core region. However taking the upper plenum as a whole the relative temperature differences between the core and upper plenum structures are fairly similar in the two code predictions.

Some of the differences above relate to differences between the codes in their modelling of the irrecoverable frictional losses associated with the internal structures.

Also, the Boussinesq assumption in which variations of density (except in buoyancy) are ignored gives rise to significant error in the velocity field since the variation of density over the range considered is ~ at least a factor of 2.

4.2.2 Effects of Recirculation Rate on the Vessel Heat-up for Undisturbed Geometry

Two calculations are considered. For the close comparison case considered above the correlations for the frictional pressure-drops were chosen to approximately correspond to those used in the MELPROG calculations. It was not possible to do this exactly due to an anisotropic formulation in MELPROG. (FLOW3D is only isotropic in this respect).

To maximise the possible recirculation rate a FLOW3D calculation was performed in which the frictional pressure drop only depended on a smooth tube correlation with a hydraulic diameter commensurate with flow through rod bundles assuming a 1-D flow and ignoring all additional losses associated with grid area.
expansions/contractions etc. To minimise the recirculation rate the frictional pressure drops were increased to levels commensurate with strong cross flows in rod bundles, together with the additional losses of grids etc included.

The difference between these two cases correspond to an order of magnitude difference in frictional pressure drops, with the FLOW3D close comparison prescription a factor of 5 higher than the low resistance case and a factor of 2 lower than the high resistance case.

Table 3 shows a comparison between the two results. The flow velocities lie within the range 1-2 m/s. For the reasons already discussed the temperature rise rates are unaffected by the recirculation rates, whereas the relative temperature differences are affected. Table 3 shows that the upper plenum structures/core temperature differences could be affected by up to about 100 K depending on the uncertainties of the flow resistance. However, the high resistance range is thought to be most realistic. The MELPROG results correspond more closely to this range and therefore the MELPROG temperature predictions are thought to be reasonably realistic at least within the confines of undisturbed geometry.

As already stated in the context of MELPROG, it is very difficult to ascribe suitable flow frictional losses in a vessel with complex internal structures. Even if the basis of the correlations themselves, largely derived and validated for forced flow conditions can be validated by experiment for the buoyancy flow conditions under consideration here, there is still likely to be uncertainty in the vessel heat-up rate due to the difficulty in predicting the exact flow recirculation.

4.3 Three-Dimensional FLOW3D Calculations

These are the main reasons behind using FLOW3D. Calculations have been carried out for a 180 degree sector of the vessel to investigate the effects of 3-D flow patterns. The plane of symmetry contains the vessel axis and the axes of the two hot legs.

The basic axial and radial mesh used are as described for the 2-D case. An azimuthal mesh of five meshes has been used, Figure 33.

To first provide a link with the two-dimensional calculations, a three-dimensional calculation has been carried out in which the 3-D features of the upper plenum geometry ie the flow holes have been smeared in order that the geometry becomes 2-D. It might be expected therefore that the results in each of the azimuthal sectors would be the same and coincide with the two-dimensional case. The results are shown in
Figure 38. They essentially agreed with their 2-D counterparts of Figures 34-38. The recirculating flows were broadly the same although in the 3-D case the flows were slightly higher (peak flows ~ 1.5 m/s as opposed to 1.3 m/s).

There was some concern whether numerical instabilities associated with weak azimuthal gradients would cause asymmetries. However, for the radial frictional losses used here, thought to be appropriate for rod bundle geometry with cross flows, asymmetries were not present, although the results did show an increased tendency towards asymmetry for reduced radial frictional losses.

4.3.1 Simulation of Flow for an Undisturbed Core Prior to Material Relocation and Prior to Pump Transient

This is a reference case containing a detailed representation of the upper plenum cylinder flow holes, and gives a prediction of the resulting flow for an undisturbed core. Thus the effects of material blockage are not represented in this reference 3-D calculation.

Figure 39 shows results in the recirculation field predicted. The Sectors relate to Figure 33.

The results are symmetric about Sector 3. Strong recirculation exists between the core and the upper plenum. The upward velocity at the grid plate elevation is ~ 1.36 m/s in Sectors 1 and 5. Stronger upward flows 1.53 m/s are observed in regions away from the hot legs, e.g., Sector 3. The presence of reverse flow, from the exterior of the plenum cylinder, through the holes opposite the hot leg, and down into the vessel is clearly seen in Sections 1 and 5.

Figure 40 shows the temperature distribution at the upper grid plate. The core temperatures at this time were around 2000 K. There is about a 20K increase in the outer core region in Sector 3 away from the hot legs, compared with the Sectors 1 and 5 near the hot legs.

In the inner core, the azimuthal temperature gradient is flat.

4.3.2 Simulation of Flow for an Undisturbed Core Prior to Material Relocation and Prior to the Pump Transient - Fine Mesh

This calculation was similar to the previous case except that the axial and radial meshes were refined by a factor of 5.

This finer mesh calculation gave similar trend results.
to the coarse mesh case though some differences were noted.

The velocities in the fine mesh case were \( \sim 25\% \) higher than in the coarse mesh case, presumably as a result of reduced numerical diffusion associated with the low order difference scheme.

There were more radial rings exhibiting up-flow in Sector 3 away from the hot legs than in Sectors 1 and 5. This could not be seen in the coarse mesh case.

The fine mesh case showed greater azimuthal variation in the manner expected than was found in the coarse mesh case, again probably due to reduced numerical diffusion. However, the large differences were over a small number of radial meshes and elsewhere the differences were still small compared with those in the coarse mesh blockage calculation, see Section 4.3.4.

There were no apparent numerical problems. The results were symmetric.

An additional calculation, including also a factor 2 refinement of the azimuthal mesh, was partially carried out but revealed little new. It was not pursued since the number of meshes were now making the computing times very long.

4.3.3 Simulation of Ballooning Prior to the Pump Transient – Fine Mesh

In this case about \( \frac{2}{3} \) of the core area was assumed to have ballooned to 90\% flow blockage. Axially the ballooned region extended from \( \frac{1}{4}\)-way to \( \frac{3}{4}\)-way up the core region. This was the region calculated to be at the highest temperature prior to the ballooning phase. In all other respects, this case corresponded to the previous calculation in Section 4.3.2: A fine mesh was chosen in order to allow bypass effects to be adequately modelled.

The ballooning certainly affected the radial distribution of the flow in the ballooned region of the core. Bypass velocities \( \sim 50\% \) greater than in the unballooned case were found. However, the flow at the level of the upper grid plate was still relatively 2-D, much as in the unballooned case.

The results for this ballooning case were broadly symmetric, though some evidence of azimuthal spatial oscillation in the results was found over a limited zone. In this region, the azimuthal gradients were less than in the unballooned case, though numerical oscillations in the ballooned case obscured the situation. Elsewhere the azimuthal gradients were
similar to the unballooned case. The implication would seem to be that overall, azimuthal effects associated with ballooning at the level of the upper grid plate are of similar order to the unballooned case.

Earlier 2-D calculations assuming ballooning (90% flow blockage) over the whole core radially, produced temperature differences between the core and upper grid plate as much as 200 K higher during the core heat-up if ballooning was present, compared with the unballooned case. However in this more detailed calculation including a by-pass the effects on the relative core/plenum heat-up are much less pronounced.

The conclusion on the effects of ballooning on natural circulation would seem to be that it only has a significant effect on the relative core/plenum heat-up if there is a substantial eg 90% flow blockage over a large fraction of the whole core. The effects of ballooning are much reduced if a flow bypass exists.

4.3.4 Simulation of Flow After Material Relocation but Prior to the Pump Transient

By the time of the B-loop pump transient the indications are that the core had reached high temperature > 2,000K. Responses from incore instrumentation and source range monitors and observations of the end-state indicate that a region of partially molten core materials had formed by this time, supported by a crust of previously molten material.

This case is intended to represent the flow situation after material relocation has occurred but prior to the pump transient. A region of low porosity has been assumed near the bottom centre of the core for the FLOW3D calculation. This is to simulate blockage by previously molten material. As for the unblocked calculation, recirculation exists between the core and upper plenum but at a slightly reduced rate (maximum upward flow 1.37 m/s at the grid plate compared with 1.53 m/s for nominal geometry). However, the relative upflow in Sector 3 compared with that in Sectors 1 and 5, Figure 41, is greater than that in the nominal geometry case, Figure 39.

This is also reflected in the temperatures, Figure 42. In the peripheral cells of the core the temperatures are over 200K hotter in the cells away from the hot legs compared with those adjacent to the hot legs. Nearer the centre the corresponding figure is ~ 50K. Thus, if a substantial region of the core is blocked, as would seem to have been the case just prior to the pump transient, the calculations are consistent with the observed damage patterns on the upper grid plate, Figure 43, [7].

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It should be noted that there is some evidence of asymmetry between Sectors 1 and 5. This seems to be characteristic of all calculations with a coarse mesh attempted with substantially increased centre core porosity. However this asymmetry is not seen in the finer mesh calculation, Figure 44.

The calculations show that the effect of increase as in the more complete core blockage case no to enhance the aximuthal variation int nthe flow higher up the case. It is thought that this is because the aximuthal recirculation in the core induced by re-entrant flow at the hot leg plenum inlets is more imped by a solid material blockage (represented by a 99% flow blockage) than by a lesser ballooning blockage (represented as a 90% flow blockage) or an unobstructed core.

4.3.5 Simulation of High Flow After Material Relocation During the Pump Transient

It has been demonstrated that the observed damage to the upper grid plate, Figure 43, is consistent with 3-D natural circulation flows that possibly developed during the early heat-up phase, prior to any material relocation. These flows arose because of complex flow paths associated with the arrangement of the flow holes in the plenum cylinder.

In [7] it is suggested that the damage resulted from operation of the B-loop primary pump, and thus was caused by an alternative mechanism to natural circulation. The mechanism proposed was bifurcation of the flow associated with the upper plenum geometry. However no 3-D calculation of the flow following pump operation had been performed to support this thesis.

The calculations below address this issue. 3-D FLOW3D calculations are presented to illustrate the nature of the possible forced-flow patterns that would result following the large steam generation rates that would have been produced during the pump transient.

After initiation of the pump, water was injected into the vessel ([8]). A volume of ~ 29.3 m$^3$ [8] was pumped into vessel over a 5 s period. This is equivalent to a rate of 4,000 kg/s. This probably caused fragmentation of the embrittled fuel rods which then collapsed forming the upper debris bed on top of the lower blockage, and a voided region at the top of the core. This surge of water would cause high temperature steam and hydrogen to exit the core and flow upwards towards the upper plenum. This aspect of the flow has been simulated using FLOW3D.

It has been observed that the effect of the pump was to increase the pressure on the secondary side of the
B-loop steam generator indicating primary to secondary side heat transfer through condensation of saturated steam. There was no corresponding increase in pressure on the secondary side of the A-loop steam generator. Since there was no flow path out of the PORV at this time (on the A-loop) the implication is that most of the steam exiting the core was through the B-loop hot leg. This assumption has been made in this nominal high flow FLOW3D calculation.

Further, from the measured primary system pressure it has been possible [7] to deduce the net increase rate in steam mass, which, if added to the rate of condensation in the B-loop steam generator on the primary side, then gives a measure of the total steam generation rate. Figure 45 shows that the steam generation rate during the first few tens of seconds of the B-loop pump transient derived on this basis was ~ 400 kg/s.

For FLOW3D calculations to describe the steam flows in the upper plenum once the pump was switched on, an inlet steam flow rate is assumed commensurate with this steam generation rate, thought to have been generated in the early stages of the pump transient. Figure 31(b) shows a schematic of the principal flow directions, which should be contrasted with the buoyancy driven case, Figure 31(a). The steam is assumed to have the enthalpy of water at saturation, since the pump would have delivered saturated water to the core. (This is rather an academic point when forced flows are under consideration). Clearly also the quench and two-phase water/steam thermal-hydraulics are quite beyond the FLOW3D calculations considered here. However, it is thought that the single phase approach with FLOW3D should enable a reasonable prediction to be made of the flow paths and temperatures in the upper plenum, at least during the early stages of the pump transient.

The calculation therefore represents a simulation at a time soon after the pump was turned on. Taking the end of the previous blockage calculation as a starting point, an inlet flow commensurate with 400 kg/s was imposed. The flow exits the vessel through Section 1, Figure 46 to simulate the flow through one hot leg only.

There is a strong upward flow through the upper grid plate of ~ 7.3 m/s in each azimuthal Sector, Figure 46. Unlike the case of natural circulation there is little variation azimuthally, at the level of the grid plate.

In the temperature profiles, Figure 47, the azimuthal gradients still persist after 6 s, much reduced in the peripheral regions, though if anything slightly larger at the centre. Some asymmetry is observed higher up the vessel due to flow through one hot leg only. This can
be seen particularly in a fine mesh calculation, Figure 48. It follows that the grid plate would probably have continued to oxidise preferentially away from the hot legs, during the first few seconds of pump operation, though by virtue of a temperature history induced by pre-existing natural circulation.

4.3.6 Simulation of High Flow-Sensitivity Calculations

A calculation was performed which was the same as the previous nominal high flow calculation except that an inlet flow of 4,000 kg/s was imposed. This is about the maximum possible steam flow, given the water flooding rate. 3-D effects associated with the upper plenum geometry and single hot leg flow were only found at the top of the vessel. The flow at the grid plate was entirely 2-D, ie no azimuthal variation.

Another calculation was performed similar to the nominal high flow calculation, except that an inlet flow of just 40 kg/s was assumed. Thus the latter two calculations bound the nominal calculation over two orders of magnitude variation of steam generation rate. Even with this relatively low flow, there is little evidence of downstream effects. The 3-D nature of the flow now just propagated as far upstream as the grid plate.

Yet another calculation was performed which was similar to the nominal high flow calculation in terms of imposed flow rate except that exit flow was now assumed through both hot legs. The flow in this case was indistinguishable at the level of the grid plate from that in the nominal case. There were the expected differences at the higher elevations.

5 CONCLUSIONS

1 The MELPROG calculations show the vessel heat-up timescale is sensitive in the HPI make-up rate. The MELPROG calculations imply that the make-up rate was not larger than ~ 8 kg/s during the early part of Phase 2 and smaller than 4 kg/s during the latter part of Phase 2.

2 The MELPROG calculations imply that the upper plenum structure temperatures were below temperatures at which oxidation and melting could occur at the time of initial core melt and relocation.

3 The most likely explanation behind the lack of damage to the upper plenum is the timely operation of the B-loop pump. However, extrapolating MELPROG results from the time of initial melt progression is not conclusive because of the difficulty of estimating the effects of continued melt progression on the upper plenum heat-up
rate.

Two-dimensional, and averaged three-dimensional FLOW3D calculations indicate similar vessel natural circulation flows and core/plenum temperature differences to the corresponding MELPROG calculation for similar data and a similar mesh. Thus although detailed representation of the TMI-2 geometry is necessary for a complete understanding of the accident, a two-dimensional representation is adequate from the point of view of the global heat-up.

In the absence of ballooning, FLOW3D sensitivity calculations show that uncertainties on the flow resistance imply uncertainties up to ~100 K in the core/upper plenum structure temperature differences during the heat-up phase.

FLOW3D calculations imply the existence of complex 3-D buoyancy driven flows, induced by the upper plenum geometry, prior to the pump transient. These produce temperature variations across the upper grid plate that are consistent with the damage patterns observed. This is particularly true if large coherent core blockages are present.

Ballooning only affects the relative core/plenum heat-up if there is a substantial blockage (eg ~ 90%) over the whole core. The effects of ballooning are much reduced if a flow by-pass exists. The azimuthal variations in the flow and temperatures at the grid plate elevation, induced by ballooning, are generally similar to those found in the unballooned case, and small compared with those induced by more substantial core blockage.

The high steam flows that existed through the upper grid plate during the first few seconds of the B-loop pump operation show no azimuthal variation. The upstream influence of 3-D geometry effects on forced convection driven flow is much less than that for buoyancy driven flows.

Oxidation of the upper grid plate possibly continued for a limited period following the pump transient in the regions observed, but because of the temperature variation in the plate induced by pre-existing natural circulation flow, rather than by any three-dimensional nature in the pump flow.

The results for buoyancy driven flows show some mesh dependency on the scale of mesh normally used for MELPROG, SCDAF fuel damage calculations. Reductions of mesh by a factor of 5 showed less smeared azimuthal temperature gradients, and flows increased by ~ 25%, presumably resulting from reduced numerical diffusion, compared with the coarse mesh case. This variation with
mesh is probably acceptable from the point of view of the thermal-dynamics in itself but the implications of mesh reduction and effect on melt progression is also an important issue and is beyond the scope of this paper.

Some numerical problems have been encountered associated with the development of asymmetric solutions in some of the 3-D buoyancy driven flow calculations attempted. In one case spatial oscillations have also been observed. In most cases the problem has been resolved by using a finer mesh, and ensuring that realistic rod bundle and other structural resistance correlations are used.

There have been no numerical problems with any of the high steam generation forced flow calculations attempted.

ACKNOWLEDGEMENTS

The authors acknowledge the contribution of EG&G staff in supplying the TMI-2 accident plant data.

They also acknowledge the contribution to this work from Dr J E Kelly and other members of Sandia Laboratories, and from Dr R J Henninger and Dr J F Dearing of Los Alamos National Laboratories for their advice in running MELPROG and for their contribution in preparing the initial MELPROG TMI-2 plant decks from which this work started.

They also acknowledge the contribution to this paper from Dr G L Quarini, Dr S M Lo and Dr R A Hiley of Thermal Hydraulics Division, AERE Harwell for setting up the original FLOW3D geometry specification, and for their advice in running the code.

This work has been partly funded by the Central Electricity Generating Board under the Thermal Reactor Agreement.

REFERENCES


<table>
<thead>
<tr>
<th>Time Period (min)</th>
<th>TMI-2</th>
<th>Reference Calculation</th>
<th>Sensitivity Calculation</th>
</tr>
</thead>
<tbody>
<tr>
<td>(a) 100-110</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core uncover at</td>
<td>100-110</td>
<td>108 assumed</td>
<td>108 assumed</td>
</tr>
<tr>
<td>(min)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>(A loop hot leg</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>temp.)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>(b) 1.37</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Rod temperatures</td>
<td>1100</td>
<td>1150 centre-line</td>
<td>1050 centre-line</td>
</tr>
<tr>
<td>(K) in excess of</td>
<td>(containment radiation monitors)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Vessel coolant</td>
<td>1.5 - 2.6</td>
<td>0.9 - 1.5</td>
<td>0.9 - 1.5</td>
</tr>
<tr>
<td>level (m)</td>
<td>(source range monitor response)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Hot leg temperatures</td>
<td>A Loop 600</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td></td>
<td>B Loop 580</td>
<td></td>
<td></td>
</tr>
<tr>
<td>(c) 1.50</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Temperatures at</td>
<td>&gt; 1350</td>
<td>&gt; 1500</td>
<td>&gt; 1200</td>
</tr>
<tr>
<td>top of core (K)</td>
<td>2.9 m from bottom of active fuel (in core SPNDs)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Hydrogen generation</td>
<td>started (increasing RCS pressure)</td>
<td>40</td>
<td>10</td>
</tr>
</tbody>
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**TABLE 1:** TMI-Data Relative to Vessel Heat-up Comparison with Calculations
<table>
<thead>
<tr>
<th>Time Period (min)</th>
<th>TMI-2</th>
<th>Reference</th>
<th>Sensitivity</th>
</tr>
</thead>
<tbody>
<tr>
<td>(d) 165</td>
<td></td>
<td>Calculation</td>
<td>Calculation</td>
</tr>
<tr>
<td>Core Temperatures at lower core elevations (K)</td>
<td>&gt; 1350</td>
<td>~ saturation temperature</td>
<td>&gt; 800</td>
</tr>
<tr>
<td></td>
<td>0.76 m from the bottom of the active fuel (in-core SPNDs)</td>
<td>&lt; 1.2 m</td>
<td></td>
</tr>
<tr>
<td>(e) 174</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core liquid level (m)</td>
<td>0.6 above bottom of active fuel (source range monitor response)</td>
<td>1.2</td>
<td>~ 0.8</td>
</tr>
<tr>
<td>Core temperatures at bottom of active fuel (K)</td>
<td>&lt; 850 K below 0.24 m (in-core SPNDs)</td>
<td>~ saturation temperature</td>
<td>~ saturation temperature</td>
</tr>
<tr>
<td></td>
<td></td>
<td>&lt; 1.2 m</td>
<td>&lt; 0.8 m</td>
</tr>
</tbody>
</table>

(f) Over Whole Accident

| Upper grid plate temperature (K) | Lower surface to two major damage zones I to hot legs ~ 1700 K | 700-1450 peripheral centre mean temperature by 158 min | 730-1370 peripheral centre mean temperature by 170 min |
| Control rod lead screw temperature (K) |       |       |       |
| Core centre-line | 1255 - 666 immediately above hot leg core elevation |       |       |
| Core periphery | 1033 - 723 |       |       |

TABLE 1: TMI-Data Relative to Vessel Heat-up Comparison with Calculations

Cont'd


<table>
<thead>
<tr>
<th></th>
<th>MELPROG</th>
<th>Flow3D</th>
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<tbody>
<tr>
<td></td>
<td>Core</td>
<td>Plenum</td>
</tr>
<tr>
<td>Maximum Axial Velocity (m/s)</td>
<td>0.4</td>
<td>1.1</td>
</tr>
<tr>
<td>Maximum Radial Velocity (m/s)</td>
<td>0.1</td>
<td>0.5</td>
</tr>
<tr>
<td>Structure/gas temperature rod difference (K)</td>
<td>25</td>
<td>200</td>
</tr>
<tr>
<td>Temperature rise rate (K/s)</td>
<td>0.26</td>
<td>0.16</td>
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<table>
<thead>
<tr>
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<th>Node</th>
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<tr>
<td>Temperature difference between core and upper grid plate centre-line (K)</td>
<td>370</td>
</tr>
<tr>
<td>440</td>
<td>6</td>
</tr>
<tr>
<td>Temperature difference between core and core support plate</td>
<td>625</td>
</tr>
<tr>
<td>520</td>
<td>10</td>
</tr>
<tr>
<td>Temperature difference between core and plenum cylinder (K)</td>
<td>400</td>
</tr>
</tbody>
</table>

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### TABLE 3: Comparison Between Low and High Recirculation Rates in FLOW3D Calculations ~ 1400K

<table>
<thead>
<tr>
<th></th>
<th>Low Resistance</th>
<th>High Resistance</th>
</tr>
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<tr>
<td></td>
<td>Core</td>
<td>Plenum</td>
</tr>
<tr>
<td>Maximum Axial Velocity (m/s)</td>
<td>1.9</td>
<td>2.2</td>
</tr>
<tr>
<td>Maximum Radial Velocity (m/s)</td>
<td>0.8</td>
<td>0.9</td>
</tr>
<tr>
<td>Structure/gas temperature rod difference (K)</td>
<td>10</td>
<td>280</td>
</tr>
<tr>
<td>Temperature rise rate (K/s)</td>
<td>0.30</td>
<td>0.25</td>
</tr>
<tr>
<td>Temperature difference between core and upper grid plate entre-line (K)</td>
<td>60</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>360</td>
<td>7</td>
</tr>
<tr>
<td>Temperature difference between core and core support plate</td>
<td>420</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>520</td>
<td>11</td>
</tr>
<tr>
<td>Temperature difference between core and plenum cylinder (K)</td>
<td>430</td>
<td></td>
</tr>
</tbody>
</table>
FIG. 1 MELPROG GEOMETRY AND CONTROL VOLUMES
FIG. 2 MELPROG REFERENCE CALCULATION - DOWNCOMER COLLAPSED WATER LEVEL

FIG. 3 MELPROG REFERENCE CALCULATION - FUEL ROD AXIAL TEMPERATURE PROFILE RING 1.
FIG. 4  MELPROG REFERENCE CALCULATION - FUEL ROD AXIAL TEMPERATURE PROFILE RING 2

FIG. 5  MELPROG REFERENCE CALCULATION - FUEL ROD AXIAL TEMPERATURE PROFILE RING 3
FIG. 8 MELPROG REFERENCE CALCULATION - UPPER GRID PLATE TEMPERATURE.

FIG. 9 MELPROG REFERENCE CALCULATION - CORE EXIT GAS TEMPERATURE.
FIG. 10  MELPROG REFERENCE CALCULATION - UPPER PLENUM TEMPERATURE PROFILE RING 1

FIG. 11  MELPROG REFERENCE CALCULATION - UPPER PLENUM TEMPERATURE PROFILE RING 2
FIG. 12 MELPROG REFERENCE CALCULATION - UPPER PLENUM TEMPERATURE PROFILE RING 3

FIG. 13 MELPROG REFERENCE CALCULATION - HYDROGEN PRODUCTION RATE.
FIG. 14 MELPROG REFERENCE CALCULATION - TOTAL HYDROGEN PRODUCED

FIG. 15 MELPROG REFERENCE CALCULATION - FRACTION OF ZIRCALOY OXIDISED.
FIG. 16  MELPROG SENSITIVITY CALCULATION - DOWNCOMER COLLAPSED WATER LEVEL

FIG. 17  MELPROG SENSITIVITY CALCULATION - FUEL ROD AXIAL TEMPERATURE PROFILE RING 1
FIG. 18 MELPROG SENSITIVITY CALCULATION - FUEL ROD AXIAL TEMPERATURE PROFILE RING 2

FIG. 19 MELPROG SENSITIVITY CALCULATION - FUEL ROD AXIAL TEMPERATURE PROFILE RING 3
FIG.20 MELPROG SENSITIVITY CALCULATION - UPPER SUPPORT PLATE TEMPERATURE

FIG.21 MELPROG SENSITIVITY CALCULATION - WALL TEMPERATURE PROFILE RING 4
FIG. 22 MELPROG SENSITIVITY CALCULATION - UPPER GRID PLATE TEMPERATURE

FIG. 23 MELPROG SENSITIVITY CALCULATION - CORE EXIT GAS TEMPERATURE.
FIG. 24 MELPROG SENSITIVITY CALCULATION - UPPER PLENUM TEMPERATURE PROFILE RING 1

FIG. 25 MELPROG SENSITIVITY CALCULATION - UPPER PLENUM TEMPERATURE PROFILE RING 2
FIG. 26  MELPROG SENSITIVITY CALCULATION - UPPER PLENUM TEMPERATURE PROFILE RING 3

FIG. 27  MELPROG SENSITIVITY CALCULATION - HYDROGEN PRODUCTION RATE
**FIG. 28** MELPROG SENSITIVITY CALCULATION - TOTAL HYDROGEN PRODUCED.

**FIG. 29** MELPROG SENSITIVITY CALCULATION - FRACTION OF ZIRCALOY OXIDISED.
FIG. 30 FLOW3D REPRESENTATION OF TMI-2 CORE AND UPPER PLENUM.
FIG. 31 PLENUM ASSEMBLY WITH A TYPICAL CONTROL ROD GUIDE ASSEMBLY.
FIG. 32 FLOW3D (R,Z) GRID FOR TMI-2 CALCULATION
FIG. 33 DETAILS OF 3-D UPPER PLENUM GEOMETRY FOR FLOW3D
FIG. 34. MELPROG/FLOW3D CLOSE COMPARISON CALCULATION TEMPERATURES
FIG. 35 MELPROG/FLOW3D CLOSE COMPARISON CALCULATION VELOCITIES
FIG. 36 MELPROG/FLOW3D CLOSE COMPARISON CALCULATION - FLOW VECTORS
FIG. 37 MELPROG/FLOW3D CLOSE COMPARISON CALCULATION-CONTOURS
FIG. 38 FLOW3D CALCULATION. 3D SMEARED PLENUM GEOMETRY
Fig. 39 Flow vectors for buoyancy-driven flow. Undisturbed geometry.
FLOW3D SIMULATION OF TEMPERATURES PRIOR TO MATERIAL RELOCATION AND PRIOR TO PUMP TRANSIENT

FIG. 40 TEMPERATURE DISTRIBUTIONS ON THE UPPER GRID PLATE. BUOYANCY-DRIVEN FLOW. UNDISTURBED GEOMETRY.
FLOW3D SIMULATION OF FLOW AFTER MATERIAL RELOCATION BUT PRIOR TO PUMP TRANSIENT

VECTORS
Y AXIS 18.8
Y INTERVAL 2.50
X AXIS 4.62
X INTERVAL 5.00
CUTOFF 250E-01

VECTORS
Y AXIS 18.8
Y INTERVAL 2.50
X AXIS 4.62
X INTERVAL 5.00
CUTOFF 250E-01

VECTORS
Y AXIS 18.8
Y INTERVAL 2.50
X AXIS 4.62
X INTERVAL 5.00
CUTOFF 250E-01

ELEVATION (m)

RADIUS (m)
FIG. 26 FLOW PATTERN SECTOR 1

RADIUS (m)
FIG. 28 FLOW PATTERN SECTOR 3

RADIUS (m)
FIG. 26A FLOW PATTERN SECTOR 5

CORE BLOCKAGE

FIG. 41 FLOW VECTORS FOR BUOYANCY-DRIVEN FLOW. CORE BLOCKAGE.
FLOW3D SIMULATION OF TEMPERATURES AFTER MATERIAL RELOCATION BUT PRIOR TO PUMP TRANSIENT

FIG. 42 TEMPERATURE DISTRIBUTIONS ON THE UPPER GRID PLATE.
BUOYANCY-DRIVEN FLOW, CORE BLOCKAGE.
FIG. 43 AZIMUTHAL LOCATIONS OF MAIN FLOW HOLES IN THE PLENUM CYLINDER IN RELATION TO PLENUM ASSEMBLY DAMAGE PATTERN.
FLOW3D SIMULATION OF FLOW AFTER MATERIAL RELOCATION BUT PRIOR TO PUMP TRANSIENT

VECTORS
Y AXIS 96.3
Y INTERVAL 12.8
X AXIS 23.7
X INTERVAL 2.56
CUTOFF 640E-01

FIG. 29 FLOW PATTERN
B-LOOP SECTOR

FIG. 30 FLOW PATTERN
90 DEG. SECTOR

FIG. 31 FLOW PATTERN
A-LOOP SECTOR

FIG. 44 FLOW VECTORS FOR BUOYANCY-DRIVEN FLOW CORE BLOCKAGE - FINE MESH
FIG. 45 STEAM GENERATION RATE IN THE REACTOR CORE DURING THE 2-B PUMP TRANSIENT. TIME 0 = 175 MIN. 9s AFTER TURBINE TRIP.
FLOW3D SIMULATION OF HIGH FLOW AFTER MATERIAL RELOCATION DURING THE PUMP TRANSIENT

VECTORS
Y AXIS  113
Y INTERVAL 15.0
X AXIS  27.7
X INTERVAL 3.00
CUTOFF 250E-01

VECTORS
Y AXIS  113
Y INTERVAL 15.0
X AXIS  27.7
X INTERVAL 3.00
CUTOFF 250E-01

VECTORS
Y AXIS  113
Y INTERVAL 15.0
X AXIS  27.7
X INTERVAL 3.00
CUTOFF 250E-01

FIG. 26 FLOW PATTERN
SECTOR 1

FIG. 28 FLOW PATTERN
SECTOR 3

FIG. 26A FLOW PATTERN
SECTOR 5

RADIUS (M)
ELEVATION (M)

CUT OFF 250E-01

FIG. 46 FLOW VECTORS FOR FORCED CONVECTION FLOW, CORE BLOCKAGE
FLOW3D SIMULATION OF HIGH FLOW AFTER MATERIAL RELLOCATION DURING THE PUMP TRANSIENT

FIG. 29 FLOW PATTERN
B-LOOP SECTOR

FIG. 30 FLOW PATTERN
90 DEG. SECTOR

FIG. 31 FLOW PATTERN
A-LOOP SECTOR

FIG. 48 FLOW VECTORS FOR FORCED CONVECTION FLOW. CORE BLOCKAGE - FINE MESH.
COMPUTER CODE DESCRIPTION (MELPROG-PWR/MOD 1)

J N LILLINGTON

THERMAL-HYDRAULIC CALCULATIONS TMI - 2
ACCIDENT ANALYSIS [A-2]

SAFETY AND ENGINEERING DIVISION
AEE WINFRITH
January 1990
APPENDIX A

COMPUTER CODE DESCRIPTION

MELPROG-PWR/MOD1 [A-1] was the integrated mechanistic code used for part of the analysis [A-2]. A brief description of the code, together with other relevant information concerning the UKAEA application of the code to TMI-2 is given below.

The overall objective of the MELPROG development is to provide a complete description of the core and in-vessel phenomena, that occur under severe accident conditions, from accident initiation up to possible failure of the vessel. MELPROG has also been coupled to the TRAC-PF1 [A-3] thermal-hydraulics code to provide a complete analysis capability for the vessel and the remainder of the primary circuit. Thus the integrated MELPROG/TRAC system can be used to predict the state of the core as a function of time, to provide thermal-hydraulic information as an input to fission product transport studies, to predict the state of debris entering the containment in the event of lower head failure, and to determine hydrogen production.

MELPROG treats 2-D vessel thermal-hydraulics, oxidation of structures, in particular Zircaloy cladding oxidation, melt progression, debris formation, melt attack and failure of structures, melt/water interactions and vessel failure.

The main thrust of the UKAEA analysis of the TMI-2 accident involved investigation of natural circulation and heat transfer between the gas and structures in the vessel, and in addition to MELPROG analysis, involved detailed analysis using the FLOW3D code [A-2]. This is described in a separate Appendix, Appendix B. The MELPROG models or modules that are relevant to understanding the analysis in the UKAEA contribution [A-2] are as follows:

FLUIDS Module

The FLUIDS module allows for four fluid fields (gas, water, solid corium, liquid corium). The geometry is two dimensional. Separate momentum and energy equations are solved for each field and separate mass conservation equations are solved for a number of species (a field can have more than one species). All species in a particular field are in thermal and dynamic equilibrium (eg hydrogen and steam in the gas phase.

Concerning the modelling of natural circulation in particular, a range of correlations relating to gas/structure heat transfer and frictional drag are required to close the system of equations.

For single phase flow of steam and hydrogen, the effective convective heat transfer coefficient to structures is taken to be the maximum of Dittus Boelter, [A-1], [A-4], (turbulent forced convection) and McAdams [A-1], [A-5] (turbulent natural convection. Averaged physical properties are used in these correlations.

The basic frictional drag is determined by a friction factor correlation of Rohsenow et al [A-1], [A-6]. Separate wall
friction coefficients are calculated for the axial and radial directions. There is considerable scope for variation, see description of the STRUCTURES module.

**CODE Module**

This treats all structures within the reactor core, fuel rods, control rods, grid spacers etc. The code allows for energy generation via decay heat and chemical reactions, in particular the Zircaloy and stainless steel steam interactions. The oxidation kinetics for these interactions are based on Prater and Courtright [A-1], [A-7] and White [A-1], [A-8] respectively.

**STRUCTURES Module**

This module treats all mechanical and thermal behaviour of the structures in the vessel external to the core. Only the thermal response was considered in the UKAEA study. A number of general structural types are available, enabling a detailed geometrical representation of the vessel to be developed. Concerning the feed back to the thermal hydraulics, hydraulic diameters and flow areas are calculated which take into account the presence of walls or plates with orifices etc. Additional friction factors can also be defined to account for specific geometries.

**RADIATION Module**

The module treats radiation heat transfer between the core and vessel. The model is 3D and can treat intact geometry, disrupted cores, rubble beds and various combinations. The model also takes account of the emission and absorption of thermal radiation in the water, gas (steam and hydrogen) and corium fields. It is based on the net method [A-1], [A-9]. More precise details are given in Dosanjh et al [A-1].

There are many other models present in the MELPROG code which are not described here, since they were not invoked in the UKAEA analysis. A detailed and full description of these is given in Dosanjh et al [A-1].
REFERENCES


Appendix B

Batelle Columbus Division
Report

on

MARCH CALCULATIONS PERFORMED FOR THE
TMI-2 ANALYSIS EXERCISE

Presented at the

AMERICAN NUCLEAR SOCIETY
TOPICAL MEETING

on

THE TMI-2 ACCIDENT MATERIALS BEHAVIOR
AND PLANT RECOVERY TECHNOLOGY

by

Roger O. Wooton

October 31 - November 4, 1988
Washington, D.C.

Battelle Columbus Division
505 King Avenue
Columbus, Ohio 43201
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ABSTRACT

Calculations with the STCP version of MARCH were performed as part of the TMI-2 Analysis Exercise. In the light of our present knowledge of what happened at TMI-2, a number of modeling enhancements were found necessary to interpret the accident. These enhancements and the results of calculations are described. The modified version of MARCH was able to reproduce many of the key accident signatures including the timing of core heatup, reasonable predictions of the core melt and cladding reaction fractions, the primary system pressure, and the hydrogen release to the containment which eventually produced a burn at 10 hours.
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MARCH CALCULATIONS PERFORMED FOR THE
TMI-2 ANALYSIS EXERCISE

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1.0 INTRODUCTION

As part of the validation effort for the MARCH 3 portion of the STCP
(Source Term Code Package), the Nuclear Regulatory Commission asked Battelle
Columbus Division to participate in the TMI-2 Analysis Exercise. The Analysis
Exercise is being conducted by OECD/CSNI together with the TMI-2 Accident
Evaluation Program being performed by EG&G for the Department of Energy.
Objectives of the program include understanding what happened during the
accident and to use the TMI-2 data to benchmark meltdown accident analysis
codes. For the MARCH code, at least, we found that the development of a
number of new models were required to understand the TMI-2 accident.

Battelle had previously performed TMI-2 calculations (Reference 3) in
the days and weeks shortly following the accident using the original MARCH 1.1
(Reference 4) version of the code. In our original calculations we predicted
a minimum water level in the core of about 1.4 m and that 16 percent of the
cladding reacted and 24 percent of the core melted. The minimum water level
is now known to have been about 0.6 m. Our original prediction of 16 percent
cladding reaction was known at the time to be a factor of three low based on
analysis of the hydrogen burn observed at about 10 hr. Our prediction of 22
percent core melting, however, was, at the time, uncomfortably high. There
was then the impression in the reactor safety community that melting had not occurred. However, our MARCH 1.1 models indicated calculations of more cladding reaction would also lead to even greater core melting. These original calculations indicated that the predictions of core melting and cladding reaction were very sensitive to the core water level or coolant makeup rates. It was found that nearly any answer, ranging from a prediction of no heatup at all to complete core melting, could be calculated for what were thought to be reasonable variations in the makeup rates.

A number of improvements have been made in the MARCH code since 1979; and versions MARCH 2 (Reference 5) and MARCH 3 (Reference 6) were released in 1984 and 1986. Needless to say we were curious to see if the more recent and improved versions of MARCH could do a better job of calculating the TMI-2 accident. We found during the present investigation, however, that additional modeling enhancements were needed to interpret the TMI-2 accident. Ironically, the modeling demands have become more difficult as our knowledge of what actually happened has improved. Previously, modeling inadequacies could not be definitively established because of our ignorance of the actual course of events.

The Accident Evaluation Program at EG&G has been the source of some major new insights and information. These new insights are largely the result of the core and reactor vessel examinations which have taken place. These new insights include:

- Nearly half of the core melted
- Portions of the core remained molten even after it was recovered with water
- About a third of the molten material relocated to the lower head at 224 min
- The peak core temperatures approached the melting point of the UO₂ fuel.
- The water coolant reached a minimum level of about 0.6 m above the bottom of the core.

Future examinations are expected to shed light on the attack of the relocated core melt on the lower head of the reactor vessel. These core examinations provide additional boundary conditions which the accident codes must consider. Unfortunately, the Evaluation Program was not able to supply significant new information of the coolant makeup to the vessel during the crucial 100 to 200 min period of core uncovering. The coolant makeup rate history continues to be an area of major uncertainty.

Figure 1 shows the primary system pressure response during the first 300 min of the TMI-2 accident. Key events in the accident sequence are indicated on the figure. For purposes of the Analysis Exercise, the accident was divided into four time periods or phases. During the Phase 1 time period between 0 and 100 min, the primary coolant pumps continued to operate, and the core remained well cooled. During the Phase 2 time period between 100 and 174 min, the core uncovered, heated up, and began to melt. Phase 3 began with the attempt to operate the primary coolant pump in the 2-B loop at 174 min. Finally, Phase 4 began with the start of full Emergency Core Cooling (ECC) flow at 200 min. At 224 min, a 20 ton portion of the molten core relocated to the lower head. Detailed descriptions of the TMI-2 accident are contained in References 8 and 9.

This report describes the TMI-motivated modeling enhancements to MARCH 3 and discusses the results of a number of calculations performed for the Analysis Exercise Program. The MARCH code and the modeling enhancements are described in Section 2.0 The boundary conditions and input data needed by
MARCH for the TMI-2 calculations are discussed in Section 3.1 and results are discussed in Section 3.2. Conclusions are presented in Section 4.0.

2.0 THE MARCH CODE

The MARCH code was written to calculate thermal-hydraulic phenomena in the reactor and containment building for accidents leading to complete core meltdown. MARCH was intended to be part of a risk assessment code package rather than a detailed phenomenological code. Since there is frequently a need to perform many sensitivity calculations in a risk assessment study, MARCH was designed to be fast-running. This was accomplished by using simple representations of physical properties data and models of phenomena that either (1) were judged to be unimportant or (2) were not sufficiently well understood or supported by experimental data to justify sophisticated models. In the latter case, MARCH frequently contains user selected options to choose among a number of alternative simplified models. Section 2.1 contains a brief description of the MARCH core and primary system models in order to help the reader better understand and interpret the results to be presented later. More detailed descriptions of all the models are contained in References 5 and 6. The TMI-2 motivated MARCH modeling enhancements are described in Section 2.2.

2.1 MARCH CORE AND PRIMARY SYSTEM MODELS

The core is represented in MARCH by parallel fuel rods which are grouped according to their power generation rates into axial and radial
lumped-mass nodes. A mixture level divides the core into water and gas cooled regions. Radiation, convection, and axial conduction are modeled. Core fuel material masses, heat capacities, and dimensions are input by the user. A single input temperature is used to trigger the core meltdown models. Fuel dissolution and cladding ballooning, melting, and candling are not explicitly modeled.

Cladding oxidation is modeled using the Urbanic-Heidrick (Reference 12) kinetics correlation. (Other correlations may be selected by user option.) Hydrogen blocking and steam supply limitations are considered. As will be discussed later, a surface area reduction concept is used to model heat transfer and metal-water reaction in melted core nodes.

Figure 2 is a sketch of the geometry represented in the MARCH primary system model. The primary system volume is modeled as a simple pot with water at the bottom and steam at the top. The water volume is partitioned into volumes below the core (WATBH), a dead-end volume (WDED), and a volume above the bottom of the core. The dead-end volume is used to represent water at low points in the primary system, such as in the bottom of the steam generators and at the bottom of the cold legs. The modeling assumes the water in WDED is thermally coupled to the other water in the vessel but does not drain into the bottom of the reactor vessel.

Water levels are referenced to the bottom of the core and are calculated using a single input value (ATOT) of the connected water area. Normally, this area is chosen to give a realistic water level within the core region. Since the water areas actually change outside the core region, levels above and below the core are artificial and require further interpretation to obtain physical meaning.
Breaks and valves are located at input specified elevations (YBRK and YSRV, respectively). When the water level falls below the break or valve elevation during the course of the accident, the leakage will change from liquid to gas. In the TMI-2 Analysis Exercise we have taken the time the liquid level reaches the top of the core (100 min) as a Phase 2 boundary condition; and YSRV was selected to give the correct timing.

The steam generator is modeled as a simple heat exchanger which may cool either the water space or condense steam out of the gas space. The code selects the cooling mode depending on the water levels and whether the primary coolant pumps are running.

2.2 MARCH MODELING ENHANCEMENTS

In order to perform meaningful TMI-2 calculations, it was found necessary to add a number of modeling enhancements to MARCH. Two categories of changes were made: (1) those of a largely input nature and (2) actual changes in physical models. These changes were made to the STCP version of MARCH 3 (V192) and are discussed below. Most of these changes have generic usefulness and are expected to appear in future released versions of MARCH.

Input Changes

Included in this category of changes are simple expansions of input time array table entries for the ECC flow, the steam generator secondary auxiliary feedwater flow rate, and the steam generator secondary pressure. These changes have no effect on the code operation or physics, and are not described further.
TMI-Specific Changes

Other changes of a largely input nature include the ability to specify a number of TMI-specific boundary conditions and parameters during the time period of the 2-B pump transient. A "relocation time" (the TMI-2 core relocation at 224 min) can also be specified by input. The effect of the relocation on lower head heating is not modeled and will be left for future consideration. These TMI-2 specific changes are discussed further in Section 3.2 in conjunction with the results.

Steam Condensation in Steam Generator

The primary coolant boiling algorithm in MARCH 3 does not allow the condensation rate of steam in the steam generator to exceed the coolant boiling rate. When boiling stopped, the condensation would also stop. This restriction is unrealistic and was removed for the TMI-2 version of MARCH.

Steam Generator Hydrogen Blocking

MARCH 3 calculates hydrogen blocking assuming the hydrogen is distributed uniformly throughout the primary system gas volume. For the TMI-2 calculations the model was changed to allow the hydrogen to concentrate in the active steam generator. The volume of the steam generator gas space is supplied by the user. The new model predicts higher hydrogen and lower steam pressures in the steam generator tubes. Since the condensation rate is assumed proportional to the difference between the tube and secondary steam pressures, condensation is reduced by the new model. The accumulation of hydrogen in the steam generator is calculated assuming the flow rate of steam and hydrogen into the tubes is proportional to the steam condensation rate in the active steam generator.
Cold Leg Steam Condensation

The new cold leg steam condensation model condenses steam from the primary system gas space, adds the condensed steam mass to the ECC injection, and heats the ECC water. The calculation utilizes a user specified condensation efficiency. (The condensation efficiency is the ratio of the ECC temperature change to that given by assuming the ECC water is heated to saturation.) Cold leg condensation is assumed to be stopped if the water level increases above a value which would fill the cold leg. Hydrogen blocking of cold leg steam condensation is not modeled.

Molten Core Regions

The new model for heat transfer from molten core regions in effect assumes that molten core nodes relocate and accumulate in a consolidated hemispherical region within the core. Thus, the melt geometry is an approximation to what was observed at TMI-2. Then an area reduction factor is calculated based on the surface areas of the original fuel rods and that of the consolidated hemisphere. For the TMI-2 fuel rods, an area reduction factor approaching 100 is obtained for a core melt fraction of 0.3. The area reduction factor is applied to the heat transfer and metal-water-reaction calculations in the existing models. The area reduction concept is applied to both water and gas covered fuel rod nodes. User input allows the area reduction concept to start either at the beginning or completion of node melting. The first (beginning of melt) option was chosen for the TMI-2 calculations.

Letdown Flow
A letdown flow model was added to the MARCH calculation of the mass of water in the primary system. The flow rate is defined by an input time array.

3.0 TMI-2 MARCH CALCULATIONS

The approach used in the calculations by the Analysis Exercise participants was to first perform a "base case" calculation. The base calculation would use the boundary conditions and plant data as defined by the Data Base from the EG&G Evaluation Program. Then a number of sensitivity calculations were to be performed, primarily to investigate the effects of different coolant makeup rates during Phase 2. The boundary conditions and MARCH input data are discussed in Section 3.1. Results of the MARCH calculations and comparisons with the TMI-2 data are discussed in Section 3.2.

3.1 BOUNDARY CONDITIONS AND MARCH INPUT DATA

Pertinent MARCH input data are summarized in Table I. Only data which are peculiar to the MARCH code models or are part of the sensitivity calculations are listed in Table I. Interested readers may obtain more information on the MARCH TMI-2 input from the author or on TMI-2 plant and accident data from References 7, 8, and 9. The input data are briefly discussed below.

Core Power

The TMI-2 data indicate the core remained at full power for about 8 sec. MARCH calculations indicate the primary system pressure response during the
first few minutes would be significantly changed if 1-2 fewer or greater full-power-seconds of operation had occurred. After reactor shutdown, MARCH uses the ANSI Standard decay power rather than the curve in the Data Base. They were judged to be sufficiently similar that the differences would have little effect on the calculations.

A seven region radial core power distribution was constructed for MARCH from the detailed 177 assembly results in the Data Base. The distribution was calculated by starting at the center of the core and proceeding radially outward, adding assemblies to a series of concentric boxes or rings. Each ring defined a radial power region. Note from Table I that towards the outside of the core, the individual assembly powers may differ from the average region power by nearly +/- 60 percent. Thus, the outside of the core is not particularly well represented by the assumption of angular symmetry.

**Meltdown Model**

MARCH meltdown model "A" was used in the calculations with a core melting temperature of 2960\(^{\circ}\)K. The melting temperature was selected to be reasonably consistent with the TMI-2 SFD (Reference 10), and FLHT (Reference 11) data. MARCH meltdown models are discussed in Reference 4. The area reduction concept discussed above was implemented starting at the beginning of node melting.

**Initial Water Inventory**

In the MARCH calculations, the initial primary system water inventory and the total makeup, letdown flow, and water remaining in the reactor vessel at 100 min were taken directly from the Data Base. The makeup and letdown flows
used in the calculation are listed in Table I. Because of ambiguities in the

<table>
<thead>
<tr>
<th>Table I. MARCH TMI-2 Input Data</th>
</tr>
</thead>
</table>

### 1. Core Power
A. 8 full power seconds followed by ANSI decay heat
B. 24 axial power zones
C. 7 radial zones

<table>
<thead>
<tr>
<th>Ring</th>
<th>No. of Assemblies</th>
<th>Radial Power Factor</th>
<th>Max. and Min. Average</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1</td>
<td>1.51</td>
<td>1.0/1.0</td>
</tr>
<tr>
<td>2</td>
<td>8</td>
<td>1.27</td>
<td>1.04/0.96</td>
</tr>
<tr>
<td>3</td>
<td>16</td>
<td>1.28</td>
<td>1.03/0.96</td>
</tr>
<tr>
<td>4</td>
<td>24</td>
<td>1.13</td>
<td>1.07/0.94</td>
</tr>
<tr>
<td>5</td>
<td>32</td>
<td>1.02</td>
<td>1.11/0.99</td>
</tr>
<tr>
<td>6</td>
<td>48</td>
<td>0.95</td>
<td>1.27/0.59</td>
</tr>
<tr>
<td>7</td>
<td>56</td>
<td>0.86</td>
<td>1.01/0.63</td>
</tr>
</tbody>
</table>

### 2. Make up flow rate
A. Base makeup:

<table>
<thead>
<tr>
<th>Start Time, Min.</th>
<th>Case</th>
<th>Time kg/sec</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>3</td>
<td>100-200 kg/sec</td>
</tr>
<tr>
<td>4.68</td>
<td>4</td>
<td>100-200 kg/sec</td>
</tr>
<tr>
<td>2.8</td>
<td>8</td>
<td>81 kg/sec</td>
</tr>
<tr>
<td>3.25</td>
<td>8</td>
<td>8 kg/sec</td>
</tr>
<tr>
<td>11.7</td>
<td>7</td>
<td>6.5 kg/sec</td>
</tr>
<tr>
<td>100-200 (See Phase II)</td>
<td>6</td>
<td>1220 kg/sec</td>
</tr>
<tr>
<td>267</td>
<td>5.8</td>
<td>84 kg/sec</td>
</tr>
<tr>
<td>257</td>
<td>5.8</td>
<td>84 kg/sec</td>
</tr>
<tr>
<td>235</td>
<td>6.1</td>
<td>81.7 kg/sec</td>
</tr>
<tr>
<td>217</td>
<td>9.5</td>
<td>8 kg/sec</td>
</tr>
<tr>
<td>115-155</td>
<td>7</td>
<td>456 kg/sec</td>
</tr>
<tr>
<td>174</td>
<td>7</td>
<td>456 kg/sec</td>
</tr>
<tr>
<td>174-280</td>
<td>8</td>
<td>456 kg/sec</td>
</tr>
</tbody>
</table>

### 4. Meltdown model
A. MARCH meltdown model "A"
B. Area reduction used at start of melt
C. Core melting temperature = 2980 K

### 5. Letdown flow rate
A. Start Time, min. kg/sec

| 0 | 0 |
| 7 | 0 |
| 239 | 0 |
| 243 | 0 |
| 283 | 13.4 |
| 299 | 0 |

### 6. Steam generator
A. A-secondary pressure: 8.895 MPa until 90 min; linearly decreased to 0.0005 MPa between 90 and 200 min
B. A-condensation gas volume = 21.2 m³
C. Initial secondary inventory = 30,000 kg
D. Auxiliary feedwater flow rate:

<table>
<thead>
<tr>
<th>Start Time, min. kg/sec</th>
</tr>
</thead>
<tbody>
<tr>
<td>553.6</td>
</tr>
<tr>
<td>52.8</td>
</tr>
<tr>
<td>9.44</td>
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<tr>
<td>8</td>
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<td>22.1</td>
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<tr>
<td>0.18</td>
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<td>5.51</td>
</tr>
<tr>
<td>8.995</td>
</tr>
<tr>
<td>11.6</td>
</tr>
<tr>
<td>0.998</td>
</tr>
<tr>
<td>20.5</td>
</tr>
<tr>
<td>2.5</td>
</tr>
</tbody>
</table>

### 3. Primary System
Total water = 219,500 kg
Water in WDDE = 73,400 kg
Pressurizer gas volume = 19.5 m³
PSRV elevation (YSRV) = 9.1 m
Total primary volume = 332.5 m³
MARCH definition of the PORV elevation, YSRV was selected to give a total PORV mass leakage at 100 min in agreement with that in the Data Base. The remaining water was placed in the dead-end or WDED mass. The dead-end mass obtained in this manner is surprisingly large. We estimate this water would nearly fill the volumes in the cold legs and at the bottom of the steam generators. In our initial MARCH 1.1 calculations performed in 1979, we had judged that these volumes were about half full.

Letdown Flow

The letdown flow in Table I is a simple representation of that in the Data Base. Because of the TMI-2 plant geometry, letdown flow does not drain water from the core region after the water level falls below the cold leg elevation. Because of the simple MARCH primary system geometry, this effect cannot be directly modeled. For the TMI calculations, except for its effect on the primary system gas volume, letdown flow is neglected when the legs are uncovered.

Makeup Flow

The makeup flow used in the MARCH calculations is taken directly from the Data Base. During the 100-200 min time period, makeup rates for a number of sensitivity calculations are also listed. The 4 kg/sec rate is the Analysis Exercise "base case", taken from the estimates of Anderson (Reference 13). The "NSAC" rate is also listed in the Data Base and was suggested as a sensitivity case. The "BCD" rate is one developed by the author. It produces a MARCH response in reasonable agreement with many of the TMI-2 data. Since the actual makeup rates during the 100-200 min period are not known, the validity of any of the rates cannot be established. The
total makeup in the 100-174 min period is 21170 kg for the NSAC case, 17760 kg for the 4 kg/sec base case, 13320 kg for the 3 kg/sec case, and 14280 kg for the BCD case. As will be seen, the calculations are sensitive to both the total mass and the timing of the makeup.

The BCD makeup rate also lists a 4600 kg water addition to the vessel from the 2-B pump transient at 174 min. This water addition increases the core water level about 0.6 m. The MARCH simulation of the pump transient is discussed further in Section 3.2.

Steam Generator

MARCH has only one steam generator. Thus, the steam generator data in Table I are for a lumped steam generator. The A-loop steam generator played a dominant role during important periods of the accident so modeling only one steam generator is not believed to be a serious handicap. The steam generator makeup rates are based on the values recommended by Anderson (Reference 14). For input convenience, his data were reduced to 14 time intervals. The steam generator secondary pressure is used in MARCH as a boundary condition. The pressure listed is an approximation to that of the A-loop steam generator.

When the water levels in the vessel and cold legs are below the cold leg elevation, steam condensed in the steam generators will not be "re fluxed" or flow back into the reactor vessel. Because of the simple MARCH primary system model, this refluxing behavior cannot be modeled directly. The effect of this refluxing behavior on the core water level is accounted for (input option) by not allowing the steam condensate to return to the vessel while the legs are uncovered.
3.2 RESULTS OF MARCH CALCULATIONS

Figure 3 is a comparison of the MARCH calculated primary system pressure with the TMI-2 data. The results shown were obtained using the BCD makeup rates for Phases 2 and 3. The calculation produced a minimum water level just below 0.5 m at 155 min, and predicts 36 percent of the cladding reacted and 43 percent of the core melted at some time during the accident. These results appear to be in reasonable agreement with a number of the known features of the TMI-2 data. These and other results are discussed more completely below. The four phases of the accident are discussed in order followed by a brief discussion of the containment response and the TMI-2 hydrogen burn at 10 hours.

Phase 1 Calculations (0 to 100 min)

Figures 4 and 5 compare two MARCH calculations of the vessel pressure with the TMI-2 data. The calculations were performed for initial primary system water inventories of 219500 and 146500 kg. The calculations illustrate the sensitivity of the calculated pressure to the initial coolant inventory. One calculation places 73400 kg of water in the dead-end mass (WDED) and the other essentially none. The 73400 kg WDED mass includes water expected to be in the pressurizer, in the cold legs, and at the bottom of the steam generators at the end of Phase 1 (100 min). The smaller inventory neglects this mass so that at 100 min the only water left in the system is that in the vessel itself.

In the MARCH calculation in Figure 4, the pressure peaks at 8 sec are coincident with reactor shutdown. The water is not boiling at this time, and the pressurization is due to simple thermal expansion of the liquid. The
larger water mass shows the greater volume expansion and compression of the pressurizer gas, and consequently the greater pressure. Note that in the TMI-2 Data Base, reactor shutdown is recorded as -3 sec; and the time scales are somewhat displaced from that assumed in the MARCH calculations. Figure 5 illustrates that the greater inertia of the larger initial coolant mass delays the start of coolant boiling following steam generator dryout at about 2 min.

The larger mass would be expected to give a more realistic calculation of the system Phase 1 behavior. However, during Phases 2 and 3 when the core is uncovered, the smaller mass gives a more credible picture in MARCH. This occurs for the following reason. During Phases 2 and 3 the core directly heats only the water in the vessel. However, the MARCH model heats all the water remaining in the primary system, including that in the dead-end volume outside the reactor vessel. Since our main interest in these calculations is the core heatup stages of the accident, all of our subsequent calculation will be performed with the smaller initial water inventory.

Both calculations are considered to be adequately in agreement with the data considering the simplicity of the modeling. It is apparent that a simple energy balance explains most of the features of the system pressure behavior during these first minutes of the accident.

Figure 6 compares the MARCH calculated PORV leak rate with the value in the Data Base. The MARCH calculation shows an initial 35 min period of liquid water leakage followed by a period of vapor leakage until the PORV valve is closed at 139 min. During the vapor leakage phase of the accident between about 100 and 139 min, the two leak rates are in agreement. This is to be expected since both calculations are normalized to the same rated valve capacity for steam flow. However, during the earlier period of two-phase
leakage, they disagree because MARCH does not model two-phase flow or have a
calculation of the pressurizer level. The total mass leakages at 100 min are
the same because the MARCH calculation used the Data Base leakage in its
input definition of the PORV valve location (YSRV).

At 100 min the collapsed liquid level has fallen to the top of the core.
The MARCH calculations, using the Data Base makeup and letdown flows, has
these two mass flows approximately in balance: 45000 kg for the letdown and
43950 kg for the makeup. The total PORV leakage is 99730 kg. Thus, the
total letdown and leakage has exceeded the makeup by about 100780 kg. MARCH
predicts about 40510 kg of water remain in the vessel at 100 min with
14400 kg of that in the lower head.

Phase 2 Calculations (100 to 174 min)

Figure 7 compares the MARCH calculated mixture levels in the core during
Phase 2 for the four makeup rates listed in Table I. The collapsed liquid
level at 100 min in all four cases was just at the top of the core (3.66 m).
The swollen mixture levels are seen to fall below the top of the core between
106 and 110 min. The uncovery times differ because the makeup rates differ
after 100 min. The BCD rate produces a minimum level of 0.5 m; the 3 kg/sec
rate a minimum of 1.1 m; and the 4 kg/sec rate a minimum of 1.3 m. MARCH
predicts the NSAC rate produces a minimum of 2.1 m at 137 min followed by a
decrease to 0.6 m at 174 min. The TMI-2 data indicate the minimum level
reached 0.6 m at some time in the accident. Based solely on the predicted
mixture level minimums, it is not clear which is the more credible
calculation of TMI-2. Other responses must be used to make this judgement.

The MARCH calculated decrease in mixture level after 155 min for the
NSAC makeup rate is believed to be unrealistic. The decrease is being driven
by excessive steam condensation in the A-loop steam generator during a period of high auxiliary feedwater flow. For the other makeup rates, steam condensation is being blocked by hydrogen generation. However, for the NSAC rate MARCH predicts only about 3 kg of hydrogen are produced; as a result, hydrogen blocking does not occur.

Figures 8 and 9 compare the measured TMI-2 A-hotleg temperature with the calculated hotleg gas and metal temperatures. All the gas temperature calculations show rapid increases after core uncovering. The calculated metal temperatures increase more slowly, and are in better agreement with the magnitude of the measured temperature. The measured temperature is believed to be a gas temperature. Thus, the MARCH calculated gas temperatures would appear to be high.

Figure 10 compares the calculations of vessel pressure with the TMI-2 data. The calculated pressures decrease with about the correct slope in the 100 to 130 min time period, but are about 10 percent high. During this time, the TMI data show the primary system pressure is very closely following the pressure in the A-loop steam generator. However, as seen in Figure 3, the MARCH calculation appears to miss predicting the rapid pressure decrease that occurs just prior to 100 min when the last primary coolant pump was turned off. It is not clear what phenomenology is being missed, other than that resulting from the obvious simplicity of the MARCH steam generator and primary system models.

The calculated pressures in Figure 10 pass through a minimum of about 4.8 MPa between 135 and 160 min. Except for the NSAC makeup case, the calculated pressures rapidly increase to pressures between 13.1 and 18.6 MPa at the end of Phase 2 (174 min). As discussed previously, the greater makeup in the NSAC case results in little hydrogen generation, and condensation in
the steam generators begins to reduce the calculated vessel pressures after 160 min. The measured pressures reach a minimum of 4.2 MPa at about 128 min and then gradually increase to about 10.3 MPa at the end of Phase 2. Thus, the calculated and measured pressure trends are initially in general agreement, but after 130 min the comparison becomes much more sensitive to the assumed coolant makeup rate.

It is also clear that the pressure calculations and data do not exhibit the same detail in structure. For example, at 124 min the calculations show a slight pressure increase followed by a decrease starting at 130 min. The data show a slight break and decrease at 120 min, and then the beginning of a gradual increase starting at 128 min. Closing of the PORV at 139 min has no obvious effect on the measured pressures and only a slight effect on the calculated pressures. In the MARCH calculations, the detail in pressure structure is caused nearly completely by responses to condensation in the steam generators. The condensation is in turn determined by the (input) auxiliary feedwater flow rates, which are listed in Table I. Thus, some of the lack of agreement in detail may be due to inadequacies in the input data as well as the modeling detail.

The effect of hydrogen blocking on the calculated steam generator behavior is illustrated by the pressure response seen between 145 and 155 min for the BCD makeup case. As seen from Table I, there are periods of high auxiliary feedwater flow between 144.8 and 147.4 min and again between 152.3 and 178.1 min. The high feedwater flow at 145 min causes an obvious pressure decrease, but hydrogen blocking prevents a similar effect at 152 min. The TMI-2 pressure data show little evidence of this fine structure. Hydrogen blocking is calculated to become important at about 147 min. The total
hydrogen mass in the primary system at that time was calculated to be 35 kg, and 8 kg were concentrated in the A-loop steam generator.

From Table I, the BCD sensitivity case has no coolant makeup in the time period between 115 and 155 min and then resumes a 7 kg/sec makeup rate until 174 min. Examination of Figure 10 indicates an obvious motivation for constructing the makeup array in this way. Without the resumption of makeup, the BCD case would have shown no pressure increase starting at 160 min. The calculated pressure would have remained flat at about 5 MPa. Our calculations indicate a clear need for a rising water level at this time. However, the source of this water is not known. There is no obvious indication in the Data Base that ECC was resumed at this time. Pressurizer draining or the resumption of steam generator refluxing are unlikely possibilities. It is also possible that slumping of core melt into the water could have been a source of increasing steam generation. However, MARCH is predicting only a few percent core melt at 160 min. Additionally, we would have expected to see spikes in the pressure trace in this case, rather than the gradual increase observed.

Figure 11 compares the maximum calculated core temperatures for the four sensitivity cases. The three higher temperature plots all show temperatures well over 1100°K at 139 min indicating cladding failures would be expected prior to closure of the PORV. Thus, these temperatures are consistent with the observation of high radiation levels in the containment at this time. No fuel or cladding melting is predicted for the NSAC case. The other cases show core melting (that is, core temperatures over 2960°K) starting at about 145 min.

The lowest core elevation at which cladding melting is predicted is
1.0 m for the BCD case, 1.8 m for the 3 kg/sec case, and 2.3 m for 4 kg/sec makeup case. MARCH predicts minimum core melting (2960°K) elevations about 0.15 m above the cladding melt elevations. The TMI-2 data indicate the presence of relocated molten cladding and core material just above the minimum water level of 0.6 m.

Figure 12 shows the fractions of cladding reacted and core melted for the BCD makeup case. At the end of Phase 2 (174 min) this calculation predicts 33 percent cladding reaction and a core melt fraction of 23 percent. The reaction and melt plots have similar shapes during Phase 2 with different end points for the other cases. At the end of Phase 2 the 3 kg/sec makeup case predicts 30 percent reaction and 25 percent melting. The 4 kg/sec case predicts 24 percent reaction and 11 percent melting. For comparison, the core end-state conditions indicate about 50 percent cladding reaction and core melting.

In summary, calculations were performed to examine the sensitivity of the MARCH calculations to the coolant makeup during Phase 2 (100 to 174 min). The results were found to be very sensitive to the assumed makeup rates. For the cases considered the predictions ranged all the way from no melting or core damage at all to about 30 percent melting and cladding reaction. Taken as a whole, it is judged that (for the MARCH code) the BCD makeup case gives a better picture of the primary system pressure, the minimum core mixture levels, the dimensions of the overheated core regions, and the extent of core melting and cladding reaction. A reasonable calculation of Phase 2 must be performed to proceed to the later stages of the accident with any hope of performing a meaningful TMI-2 calculation. For this reason, a judgement was made to use the BCD makeup case to define the conditions for the Phase 3 and Phase 4 MARCH calculations to be discussed below.
Phase 3 Calculations (174 to 200 min)

The Phase 3 calculations have proven to be the most difficult part of the TMI-2 analysis. In particular, understanding why the TMI-2 primary system pressure remained at a relatively flat 14 MPa for a 20 min period following operation of the 2-B pump has been difficult. Both hand calculations and a number of exploratory MARCH calculations were used to investigate this phenomena. We believe we now understand what happened during this time period.

A number of interesting phenomena occurred during Phase 3. At 174 min the 2-B loop primary pump was turned on and probably moved enough water into the vessel to cover the core. A substantial fraction of the core is believed to be molten at this time; the MARCH Phase 2 calculations predict 23 percent is molten. One might guess that covering a molten core with water would produce a large and continuing generation of steam, and the vessel pressure would increase. However, the vessel pressure remained at a relatively flat 14 MPa for 20 min. This is the situation which must be understood.

A number of MARCH calculations were performed to investigate the 2-B pump transient. Generally, it was found that calculations in which large amounts of steam were generated when the core was covered with water could not be made to produce reasonable vessel pressures. The auxiliary feedwater flow rates to the steam generators were not found to be high enough to condense the steam after 178.1 min; and primary system pressures increased to the relief valve set point (17.0 MPa). Calculations performed in which the relief valves were not allowed to open indicated pressures as much as 40 percent above the measured 14 MPa. The only situation in which the steam generators were found to be effective was one in which the 2-B primary
coolant pump was assumed to actually circulate water around the loops for several minutes. However, this assumption is contradicted by the actual TMI-2 pump flow data at 174 min. Thus, steam generator heat transfer does not seem to be what keeps the TMI-2 pressure from increasing in the 20 min period after the pump transient.

These MARCH calculations suggested that either (or both): (1) the calculated heat transfer from the molten core to the water was excessive or (2) the temperature of the water added to the vessel by the 2-B pump was overestimated. These possibilities were investigated.

As illustrated in Figure 13, the new molten core heat transfer model maintains a relatively constant average core temperature during the period of core recovery with water. Thus, the net heat removal must be approximately equal to the core decay heat. This would seem to be a reasonable heat removal rate. However, considering the state of the core, heat removal rates a factor of two greater or smaller would certainly also seem reasonable. Heat transfer rates an order of magnitude greater than calculated, however, are unlikely since it is known that much molten material remained until relocation at 224 min. In reality, the state of the core is unknown and the adequacy of the core model cannot be truly judged by the TMI-2 data. The new model does accomplish its objective, however, of maintaining the core in a molten state until 224 min.

At the present time we believe the temperature of the water added to the vessel by the 2-B pump transient is the controlling factor in maintaining the initial Phase 3 pressure peak at 14 MPa. We postulate that the pump transient displaced much of the existing water in the vessel and replaced it with water from the bottom of the B-loop cold leg and steam generator. Other investigators (Reference 8) have concluded the 2-B pump moved 28 m³ water
into the vessel in 15 sec. The measured cold leg and steam generator secondary temperatures were 500 and 456°K, respectively. The steam generator primary side water temperature would be expected to be close to the secondary temperature. The MARCH calculated steam generator secondary temperature is 487°K at 174 min. Assume the water added to the vessel was at the MARCH calculated temperature and that it displaces the existing water. A simple hand calculation indicates that in order to suppress boiling for 19 min at 13.8 MPa (614°K saturation temperature) with 28 m³ water in the vessel, the heat input to the water must be less than about 56 percent of the core decay heat. Complete water replacement and filling to the cold leg elevation (69 m³ water) could suppress boiling for a heat input corresponding to 138 percent of the core decay heat. Clearly, the cold water addition is a potential mechanism for explaining the vessel pressure response during the 2-B pump transient. It is also noted that the limitations on heat input to the water are consistent with the MARCH calculations discussed above for the melted core. We believe the water displacement mechanism is the most credible explanation of the 20 min temperature plateau at the beginning of Phase 3; and we have based our Phase 3 calculations on this assumption.

MARCH has no mechanistic models which would calculate the water movement by the 2-B pump. Lacking a pump model, a simple coding modification was made which gives the user input control of the water temperature. This approach was judged to be acceptable in MARCH for these calculations. However, the approach is clearly TMI-2 specific and would not be expected to have generic usefulness.

With an assumed incremental water addition of 4600 kg, the MARCH BCD case calculation shows a total vessel water inventory of 29770 kg after the pump transient. As discussed above, the pump transient is assumed to
displace all this water (35.9 m³) and replace it with water at a (input) temperature of 487°C. Figure 14 compares the resulting MARCH calculation of vessel pressure with the TMI-2 data in the 100 to 300 min time period. The calculated pressure at 155 min is lower than the data, probably because the BCD water level and makeup rate are low at this point. In response to the increasing water level caused by the assumed resumption of ECC makeup at 155 min and the effects of the pump transient, the calculated pressure increases rapidly to about 13.5 MPa at 175 min. The pressure remains below 15.0 MPa until after 192 min when boiling is calculated to resume. A small pressure decrease is first seen at 192 min due to opening of the PORV. Pressures then increase to a value just below the 17.0 MPa setpoint for the relief valves at the end of Phase 3 (200 min). The MARCH simulation of the 2-B pump transient is seen to be in reasonable agreement with the TMI-2 vessel pressure data.

The 2-B pump transient was assumed to add 5.4 m³ (4600 kg) water to the vessel in addition to the water cooling effects discussed above. Figure 15 indicates the calculated mixture level increases above the top of the core (3.66 m) to a maximum of 4.2 m at 175 min due to the water addition, decreases due to the assumed water temperature change, and then increases to a level of 4.8 m at 192 min in response to increasing temperatures and level swell as boiling resumes. The level decreases to 2.0 m just before full ECC injection is started at 200 min.

Reference to Figure 12 indicates the calculated cladding reaction increases only slightly during Phase 3. The melt fraction increases significantly. At the end of Phase 3, 40 percent of the core remains molten (above 2960°C) and 36 percent of the cladding is reacted. Our Phase 4 calculations indicate little change in these numbers prior to the melt.
relocation at 224 min. These core melt and cladding reaction fractions would appear to be somewhat low, compared to the known TMI-2 end state conditions (about half melted and reacted).

These predictions are relatively insensitive to the assumed core melting temperature. A sensitivity calculation performed in which the assumed core melting temperature was reduced from 2960 K to 2800 K produced almost no change in the predicted core melt fraction. The predicted cladding reaction was reduced from 36 percent to 31 percent.

**Phase 4 Calculations (200 to 300 min)**

Full ECC flow is initiated at the beginning of Phase 4. As can be seen from the mixture levels in Figure 15, the core is quickly recovered. The MARCH calculations indicate the core remains covered through the end of Phase 4.

The rapid pressure decrease seen in Figure 14 starting at 200 min in the MARCH calculations is due to steam condensation on the cold ECC injection water. Note from Table I that the PORV is closed from 198 to 220 min so the observed depressurization cannot be caused by coolant leakage. At 207 min the cold legs are calculated to fill with water, and it is assumed the ECC condensation stops. Prior to this time, the ECC condensation calculation had little effect on the system pressure response. That is, the effect of steam condensation on the vessel pressure was compensated for by the increased coolant boiling caused by the higher ECC water temperature. The calculated pressure is seen to fall about 2 MPa below the measured pressure at 207 min, and then increase to be in good agreement with the data between 215 and 222 min.
At 223.5 min the primary system pressure data show the beginning of a 1.9 MPa pressure increase. This pressure increase coincides with the TMI-2 core melt relocation. MARCH calculates that 33 percent of the core remains molten at this time and another 10 percent has already resolidified. The influence of the melt relocation on the core-to-water heat transfer process was simulated in the MARCH calculations by assuming, in effect, the core melt region was no longer consolidated. Thus, the area reduction calculation for molten core nodes was skipped, and the heat transfer calculation reverted to the previous models. The core cooling obtained is consistent with what would be obtained for molten but intact fuel rods covered with water. As can be seen from Figure 12 and the temperatures in Figure 13, the melt is calculated to resolidify in a few minutes. The cooling rate obtained by this calculation is much greater than would be obtained for a debris bed and much slower than what would be predicted by a direct melt-coolant interaction (that is, a steam explosion).

The vessel pressure data in Figure 14 show an immediate pressurization due to the relocation. The MARCH calculation shows a few minute time delay, either because the calculated water temperature was initially too low or the debris particulation was not adequately simulated. The TMI-2 data suggest there may actually have been a bimodal type of interaction with a portion of the debris cooling as particulates and a portion remaining as a relocated melt.

Recent examination of the instrument tubes in the lower head shows melting of some tubes which were covered by core debris while others close by are not melted. This suggests some of the tubes were immersed in molten debris and others in more coolable particulated debris beds. MARCH sensitivity calculations performed to examine lower head heatup and failure
indicate the lower head would be unlikely to survive contact with a molten debris pool of the size observed at TMI-2. However, calculations performed in which the debris were assumed to be in the form of a debris bed indicated survival. These calculations suggest that the non-uniform or bimodal nature of the debris is what allowed head survival.

The MARCH and TMI-2 pressure data after 224 min show a number of decreases and increases. The pressure changes can be explained by the opening and closing of the PORV while changes are also being made in the ECC flow. The system is simply responding to changes in coolant boiling rates and the compression and expansion of the gases in the primary system.

**Containment Response**

Figure 16 is a comparison of the MARCH calculated containment pressure with that measured during the TMI-2 accident. The calculated pressures are generally seen to be about 10 percent high. The calculated pressures in the first 15 minutes are high primarily because MARCH does not model the TMI drain tank. The open/close cycles of the PORV at 139, 192, 220, 260, and 276 min are apparent in the containment pressure response. The comparisons with the containment temperatures (not shown) are not as good because the measured temperatures are a location-sensitive variable while the calculated temperature is a volume-average value.

Figure 17 shows the calculated masses of hydrogen generated and remaining in the reactor vessel. It is seen that 357 kg of hydrogen are calculated to be produced. About 100 kg of hydrogen is leaked to the containment building when the PORV is open prior to 198 min. An additional 200 kg is leaked just after 220 min to bring the total leakage to 308 kg. The 1980 MARCH 1.1 calculations indicated there were additional possible
times of hydrogen release through the PORV to the containment when calculated water levels were low at 8.3 and 10.0 hours.

A hydrogen burn was observed in the TMI-2 containment just before 10 hours. It is interesting to compare the TMI-2 burn pressure with that which would be predicted by the present MARCH calculations. Figure 18 compares the MARCH-calculated "adiabatic" hydrogen burn pressure with the TMI-2 pressure. (The MARCH adiabatic burn pressure is that which would be obtained under adiabatic conditions if the hydrogen in the containment at that time were burned assuming no prior combustion.) The results in Figure 18 indicate there was sufficient hydrogen in the containment shortly after 220 min to explain the size of the observed burn at 590 min. This suggests all that was lacking for an earlier burn at TMI-2 was a combustion trigger. Figure 19 compares the measured TMI-2 burn pressure at 590 min with that obtained in a MARCH calculation in which a combustion trigger was assumed at 280 min. The time scales are broken to facilitate the comparison. The magnitudes and widths of the pressure spikes are seen to be quite similar. About 74 percent or 227 kg of the hydrogen was calculated to be burned, as determined by the coded burn limits. The analysis of Henrie and Postma (Reference 15) indicates 370 kg of hydrogen were in the containment at the start of the TMI-2 burn, and 319 kg or 86 percent burned. The differences in pressure rises seen in Figure 19 are consistent with the differences in masses of hydrogen burned and the containment volumes assumed in the two calculations.

4.0 CONCLUSIONS

The objectives of the Battelle participation in the TMI-2 Analysis Exercise were (1) code assessment and (2) understanding what happened at
TMI-2. We found one objective could not be accomplished without the other.

In the light of our present understanding of the TMI-2 accident, a number of modeling enhancements to MARCH were found necessary to interpret the accident. These enhancements included:

1. a simple model of heat transfer and metal water reaction for large consolidated molten core regions,
2. an improved model of hydrogen blocking which reduces condensation in the steam generators, and
3. a cold leg steam condensation model.

Some phenomena were treated non-mechanistically. These included phenomena which were judged to be TMI-2 specific (the timing of the core melt relocation at 224 min.) or could not be reasonably attempted or incorporated in the simple MARCH representation of the primary system geometry (the 2-8 pump transient at 174 min.)

The calculations were found to be very sensitive to the coolant water level or makeup rates during the core heatup and melting period between 100 and 200 min. For the makeup rates considered in a sensitivity study, predictions ranged all the way from no melting or core damage at all to about 30 percent melting and cladding reaction. The makeup rate continues to be a major uncertainty and mitigates against use of the TMI-2 accident as a source of data for some important aspects of MARCH code validation.

For a particular choice of the makeup rate (the "BCD" rate), the TMI-2 version of MARCH was able to reproduce many of the key accident signatures including the timing of core heatup, reasonable estimates of the core melting and cladding reaction, the primary system pressures, and the hydrogen release to the containment building which lead to the burn at 10 hours. This calculation predicted a minimum water level of 0.5 m at 155 min, 36 percent
cladding reaction, and 43 percent core melting during the accident. About 33 percent of the melt was calculated to remain in the core at the time of the observed TMI-2 melt relocation at 224 min. For comparison, the TMI-2 data indicate a minimum water level of about 0.6 m, end-state core melt and cladding reaction of about 50 percent, and a melt relocation of about 15 percent of the core. The MARCH calculation also predicts the interesting result that there was sufficient hydrogen in the containment building shortly after the PORV was opened at 220 min to explain the magnitude of the hydrogen burn actually observed at TMI-2 at 590 min.

These calculations suggest that relatively simple models, such as those in MARCH, can be used to explain many of the TMI-2 accident phenomena. In particular, (1) modeling of the primary system as a one-volume pot and (2) use of a single input temperature to indicate core melting and its effects on metal water reaction were shown to be useful approximations to reality. Many of the changes made to MARCH for the TMI-2 calculations have generic usefulness and are expected to be included in future releases of the code.
Figure 1. The Four Phases and Key Events in the TMI-2 Accident

Figure 2. Sketch of the MARCH Primary System Geometry
Figure 3. Comparison of TMI-2 With a MARCH-Calculated Primary System Pressure Between 0 and 300 Min

Figure 4. Effect of Initial Coolant Inventory on the MARCH-Calculated Primary System Pressures Between 0 and 2 Min
Figure 5. Effect of Initial Coolant Inventory on the MARCH-Calculated Primary System Pressures Between 0 and 20 Min

Figure 6. Comparison of PORV Leak Rates from MARCH and the TMI-2 Data Base
Figure 7. Sensitivity of the MARCH-Calculated Core Mixture Levels to the Makeup Rate Between 100 and 174 Min

Figure 8. Comparison of the TMI-2 Hotleg-A Temperatures with the MARCH-Calculated Gas Temperatures After Core Uncovery
Figure 9. Comparison of the TMI-2 Hotleg-A Temperatures with the MARCH-Calculated Metal Temperatures After Core Uncovery

Figure 10. Comparison of the TMI-2 and MARCH-Calculated Primary System Pressures Between 100 and 174 Min as a Function of Makeup Rate
Figure 11. Sensitivity of the MARCH-Calculated Maximum Core Temperatures to the Makeup Rate Between 100 and 174 Min

Figure 12. March-Calculated Fractions of the Core Molten and Cladding Reacted Between 100 and 300 Min for the BCD Makeup Case
Figure 13. MARCH-Calculated Maximum and Average Core Temperatures Between 100 and 300 Min for the BCD Makeup Case

Figure 14. Comparison of the TMI-2 and MARCH-Calculated Primary System Pressures Between 100 and 300 Min for the BCD Makeup Case
Figure 15. The MARCH-Calculated Vessel Mixture Level Between 100 and 300 Min

Figure 16. Comparison of the TMI-2 and MARCH-Calculated Containment Pressures Between 0 and 300 Min
Figure 17. The MARCH-Calculated Masses of Hydrogen Generated and in the Vessel Between 100 and 300 Min.

Figure 18. Comparison of the TMI-2 Containment Pressure Between 0 and 800 Minutes With the MARCH-Calculated Adiabatic Hydrogen Burn Pressure.
Figure 19. Comparison of the MARCH-Calculated and the TMI-2 Hydrogen Burn Pressure Spikes
Appendix C

Commissariat A L'Energie Atomique
FINAL CSHI TMI-2 ANALYSIS EXERCISE REPORT

FRENCH PARTICIPATION

P. DUMAZ
R. SENEMEAUD - R. GONZALEZ - P. CHATELARD
I - INTRODUCTION

In the frame of the CNSI TMI-2 benchmark exercise, we present the French contribution under the form of a synthesis of the most significant results of the calculation performed with CATHARE/ICARE1 for the phase 1 and the beginning of phase 2 up to important material relocation and with ICARE2 for the phase 2. The main features of those codes are describe in appendix.

The CATHARE/ICARE1 computation results have been presented to the October 1988 CNSI TMI-2 meeting, and the ICARE2 computation results to the October 1989 meeting.

This CNSI exercise is very important because it is an opportunity of to emphasis exertion about of phenomena modeling and speeding up the development of codes able to take into account these phenomena in accident sequences.

II - RESULTS

II.1 - LOCA PHASE RESULTS
(CATHARE/ICARE1 COMPUTATION)

A - Node diagram

The CATHARE model of the TMI-2 reactor takes into account the primary circuit and secondary side of the once-through-steam-generators (OTSG). Therefore three independent circuits are modeled. A schema is given fig. 1.

* The CATHARE components used are:
  - pipe : downcomer, core bypass, core, hot and cold legs, primary sides of OTSG, surge line, "PORV line", line between pressurizer and pilot-operated-relief-valve (PORV), upper and lower boilers of OTSG.
  - volume : upper downcomer, lower plenum, upper plenum, upper head, pressurizer, water boxes, OTSG downcomers.
  - "T" : surge line junction, OTSG aspirators (bypass steam for feedwater heating).

* Each hydraulic component is associated with at least one structure.

It is assumed that there is no loss to the containment.

The core is connected to four structures:

- 3 average rods with ten axial nodes
  1 for the central region (5 200 rods)
  1 for the intermediate region (14 976 rods)
  1 for the outer region (16 640 rods)
- 1 structure for the core baffle.

We don't take into account the control rods and the grid spacers.

* the other main assumptions are:
  - the two cold legs per loops are modeled by one pipe.
  - the thermal coupling between the primary side and the secondary side of the OTSG is explicit (it is an exception to the fully implicit rule of CATHARE),
  - the flow area of the vent valve is proportional to the difference of pressure between the upper plenum and the downcomer.

* The boundary conditions of the computation are:
  - the PORV flow rate : the code computes this flow rate. The method is based on the calculation of the system's characteristics (more specifically stated, this is the system coming from the six equation model). A singular pressure drop upstream the PORV has been adjusted in order to make the calculated flow fit the experimental flow rate : 16.4 kg/s at 15.65 MPa (analysis exercise data, revision 4).
  - The feedwater of steam generators:
    normal : measured
    auxiliary : estimated or computed.
  - These unknown boundary condition is controlled in order to follow recorded OTSG levels (proportional...
ferential Controller). This flowrate is injected in the following distribution:

- 65 l injected at the bottom of the boiler
- 25 l injected at the middle of the boiler
- 10 l injected at the top of the boiler

In phase 2, we change the position of lower injection so that it is above the liquid level.

- The outlet pressure of the steam generators: measured.

- The letdown flow, the high pressure injection, the decay heat and the power of the pressurizer heaters: analysis exercise data.

- The momentum and the energy due to the primary pumps: the pump data given by INEL are related to the first quadrant in single-phase flow. For the other quadrants and for two-phase flow degradation, we have used the homologous curves of the LOFT facility pumps (because in the first quadrant, its behavior is very similar to the TMI-2 pumps). The motor torque is approximated by:

\[ C_m = \frac{C_{mn} \cdot g \cdot (a + b \cdot |g|)}{(cg)^2 + (a + b \cdot |g|)^2} \]

with:  
- \( g \) : slip
- \( C_{mn} = 43170 \) m*N: Nominal motor torque

\[ a = 0.007644 \quad \text{and} \quad b = 0.045406 \quad \text{3 parameters} \]
\[ c = 0.189626 \]

8 - Discussion of results

In the analysis exercise, the turbine trip has been chosen as time 0 of the accident. But the transient begins before this instant: the last plant computer record of the initial steady state is 6 s. Our initial steady state computation is related to this instant. It is performed in two steps:

- Calculation of the two-phase flow steady state in the secondary side of OTSG (CATHARE 1 steady state calculation option runs in single phase flow).
- Initialization of the full CATHARE model with the previous results.

The phase 1 of the accident is well simulated by CATHARE. Primary pressure (fig. 2 and fig. 3), pressurizer level (fig. 4), and RCS mass flow rates (fig. 5) agree well with the TMI-2 data.

* From minus 6 s to 8 s.

The transient begins with the increase of secondary steam pressure, about 1 MPa in 6 seconds (probable consequence of the operations on the secondary circuit). So in the boilers, the two-phase velocities decrease. In consequence, the primary to secondary heat transfer decreases (same result, if the OTSG feedwater flows are not stopped), and the primary pressure begins to increase. Shutdown of the steam generator feedwater accelerates the degradation of the heat transfer in the steam generator. So the primary pressure continues to increase up to the reactor scram (8 s).

* From 8 s to 480 s.

After reactor scram, the primary pressure and the pressurizer level decrease (liquid contraction). Our computation underestimates this pressure (fig. 2). It is a consequence of the overestimation of primary to secondary heat transfer. The dryouts of OTSG occur at 100 s in the computation and at 60 s in the accident. We think that it is due to a "bad" initial water inventory of OTSG: the liquid in the OTSG downcomer is subcooled (≈ 10°C) because the computed aspirator flow is too small (about 70 kg/s). In the plant, the OTSG downcomer is close to saturation which requires an aspirator flow of 90 kg/s.

After the dryouts of OTSG, the primary pressure is controlled by the steam volume at the top of the pressurizer (balance between the increase of the pressurizer level and the PORV flow rate).
The primary temperatures are constant after 100 s and up to 200 s in the calculation, 270 s in the accident. In the calculation, the instant 200 s is the shutdown of HPI (high pressure injection). So we don’t understand why the temperatures don’t increase in the accident. The probable explanation is that the HPI continues after 200 s (flywheel of the HPI pumps?).

When the pressurizer is filled with liquid (> 340s) the primary pressure again increases. The thermal expansion of the primary liquid can’t be absorbed by the pressurizer (the PORV flow rate is too small). This increase of primary pressure is ended when the auxiliary feedwater flows are available (480 s). The fig. 7 shows the void fraction in the circuits just before 480 s: (stratification in the upper head).

* From 480 s to 6040 s.

After a large decrease (up to 1500 s), the primary pressure tends to be stationary (about 7 MPa) and follows the secondary pressures. This "equilibrium" results from the balance between decay heat, steam generator power, PORV, make-up and letdown flows. During this phase, it is very important to correctly take into account the OTSG secondary levels because they control the primary to secondary heat transfer. This is the case in our study.

After the shutdowns of B loop pumps (74 min), the A loop controls the transient. The only function of the B loop is related to the make-up flow: this water is streaming to the vessel downcomer. At 6040 s the vapor fraction in the core is about 0.6 but the core is still being cooled (see fig. 9), just before the shutdown of the A pumps, we can notice the stratification in the B loop.

* From 6 040 s to 8 340 s:

After shutdown of the A loop pumps, the A loop continues to control the transient because a vapor circulation is generated by the steam condensation in the primary side of the OTSG-A. This condensation is due to a quick increase of the OTSG secondary level up to 7500s and also due to the let located at the bottom of cold leg, ..., which reduces the OTSG primary level.

In the core, the liquid tends toward stratification so the liquid fraction and steam production increase (by a factor 2) which slows down the primary pressure decrease. We notice that the shutdown of the A pumps has not changed the heat transfer rate in the OTSG-A.

The pressurizer level decreases because there is no more liquid in the hot leg A therefore the vapor which is generated by the pressurizer heaters isn’t compensated. The good agreement existing between computed and measured pressurizer levels, see fig. 4, is a consequence of a correct pressurizer heater power).

The core uncover begins after 6500 s (fig. 6 and fig. 11), the clad temperature increase rates are around 0.6°C/s at this time.

In the accident the decrease of primary pressure is ended at about 8 s. In the basic computation, the primary pressure continues to decrease and follows the secondary pressure of the OTSG-A.

This point has been studied in sensitivity calculations. This study shows that at 7700 s the repressurization is probably due to the difference between primary and secondary levels in OTSG-A, the effect of hydrogen occurs probably later in the accident so we suspect the "standard letdown flow" (analysis exercise boundary condition) which controls the primary level in OTSG-A.

The following table gives results for clad deformations and failures. Some comments must be done:
In order to compute the clad failure, we had to cancel the feedback of clad deformation on the gap pressure of the rod. This problem is a consequence of the large meshes (2 feet) in the region of clad rupture. A good prediction of this feedback requires very small meshes. That has been done in a separated single rod computation with the thermalhydraulic conditions given by CATHARE. The results of the previous table have been confirmed.

The computed strain at rupture is not compatible with the pitch of the fuel assembly (the code doesn't take into account rod to rod contacts) but compatible with the assembly area. So we can suspect a large flow blockage.

Times of rupture seem to be correct because the first alarm of reactivity in the containment occurred at 8064 s. So we can conclude that the clad temperatures are well predicted up to 8340 s (closure of the PORV valve).

* From 8340 s to 9160 s.

The main problem after the closure of the PORV block valve is the behavior of the pressurizer (see fig. 10, at 8760 s). In our first computations, the liquid which filled the pressurizer (= 30 m³) flowed out of this volume and rewetted the core. This problem is related to the gas velocity at the junction between the surge line and the pressurizer. Before 8340 s the vapor velocity (= 5 m/s) in the surge line is sufficient to hold up the liquid in the pressurizer. After 8340 s, this velocity is very low and lets the liquid fall down. In the condition of the calculation, a gas velocity about 0.8 m/s is required by CATHARE to hold the liquid up. After 8340 s, such a velocity requires an increase in the primary pressure (but it is not a sufficient condition). This is not the case in the basic computation (with TMI analysis exercise boundary conditions and geometry). Therefore, in order to calculate the overheating of the core in the basic computation we have decided to close the junction between the surge line and the pressurizer after 8340 s.

Concerning the fuel behavior, the main event is the oxidation escalation at 9145 s which results in the clad temperature 1580°C. This change of the oxidation kinetics could explain the increase of the primary pressure rate observed at about 9300 s. The melting temperature of the zircaloy is reached at 9160 s and we stop this basic computation when the zircaloy is completely molten at a given axial mesh.

II.2 - SFD PHASE RESULTS (ICARE2 CODE)

A - Node diagram

The data needed to perform an ICARE2 calculation can be separated into four sections according to:

A-1 - General description

channels
6 in core
1 baffle plate
core barrel
vessel
representative
fuel rods 6
control rods 6

Structures taken into account
baffle plate
core barrel
neutronic shroud
vessel
thermal shroud

A-2 - Physical assumptions of computation
- radiative heat transfer:
  NONABSORBANT GAS
- chemical interactions:
  (H₂O/solid Zr) : URBANIC
  (UO₂/solid Zr) : HOFFMANN 87
  (UO₂/liquid Zr) : POLITIS
- break criteria
  cladding : Zr ALPHA MELTING
  temperature (2250 K)
  control rod guide tube: 1750 K
  fuel loss of integrity: 2250 K
  fall down velocity of fuel debris : 1 CM/SEC
  flow down velocity of molten materials : 60 CM/SEC

A-3 - Axial nodalisation (Fig. 12)
Total number of meshes : 20
- lower part of core : 1 coarse mesh
  (including the water level after 140 mm) (Fig. 12)
- medium part of core : 10 small meshes
  (including the materials relocations after 160 mm).

A-4 - Boundary conditions
For convenience, the starting point of the SFD phase will be defined as the beginning of core uncovery that occurs at about 107 mm in the timing of the accident:

< starting time of computation
ICARE2 : 107 mm >

Four physical parameters must be provided to the code as data:
- water level decrease,
- steam pressure behavior in the core,
- steam mass flow rate,
- nuclear residual power decrease.

There are large uncertainties on the estimation of water level and steam flow rate which have been given by CATHARE.

B - Discussions of results
B-1 - Fuel rod heat up before core collapse

The main consequence of the core uncovery is the rapid fuel rod heat up; In fact, before the core collapse (up to 3000 s after the beginning of uncovery) the cladding temperature reaches 2250 K (Fig. 14). During the strong oxidation phase, the cladding heat up rate in the core central region reaches 5.7 K/s; nevertheless the cladding oxidised thickness remained less than 20 %. When cladding temperature reached 2250 °C (melting temperature of zircaloy alpha phase), the calculation oxidation stopped because the present version of ICARE2 does not take into account this chemical phenomenon on molten zircaloy. This fact explains the change of slope (to 140 mm on clad n°1) seen on the temperature behavior (Fig. 15).

In the same time as the cladding becomes oxidized on its outer side, the fuel undergoes a physical chemical attack by the zircaloy on the inner side of cladding, but UZr alloy production is not significant.

In connexion with the previous phenomena, the fluid exhibited the same heatup rate as the fuel rods except for the sixth subchannel of the core. This fact is corroborated by the examination of the cladding radial temperature profile (Fig. 16) and the axial temperature profile in vessel (Fig. 17). In this case the baffle plate temperature remained under 700 K although the core central temperature in 1200 K. The gap bet. these temperatures (500 K) is resulting
of the quantity of energy caught by the external core channels (sixth and seventh subchannel in the radial meshing in ICARE2 computation).

In the core just above the water level, the local rod temperature is saturated. On the other hand, at the top of the core, the cladding reaches zircaloy melting temperature. Consequently the axial temperature profile in the core (Fig. 17) is very steep.

B.2 - Fuel behavior during core slumping

For the calculation, the basic assumption done is:

The predicted zirconia crust generated by the oxidation of cladding is not thick enough to maintain the stack of fuel pellets when zircaloy reaches its melting temperature.

The immediate consequence is the collapse of fuel when zircaloy starts to melt. At this time (140 mn) the core ruin is effective. Few minutes later, the axial profile (Fig. 18) showed a stabilised temperature between 2.8 meters and 4 meters due to meltdown zircaloy. This last core state is showed by the view (Fig. 19).

Before continuing the analysis of computational results, it is necessary to examine the zircaloy meltdown velocity : VFLD and the fuel slumping velocity : VS. The choice of the value VFLD = 0.6 M/s was made after a sensitivity study which showed that this parameter was not important to the core ruin evolution. It seems that the slumping velocity (VS = 0.01 M/s) is not a very sensitive parameter too.

During the core slumping (Fig. 20, Fig. 21) it is seen that the resulting debris bed is very compact: this unrealistic prediction is due to the fact that in present version of ICARE2, it is not possible to account for bed porosity; its modelling is foreseen.

B.3 - Control rods behavior core degradation

During core degradation the control rods meltdown occured early because the melting temperature of silver-cadmium-indium (AIC) alloy is about 1220 K (the boron carbide control rods are not modelled in the calculation). At the steel melting temperature the guide tube lost its integrity (hypothesis of zircaloy dissolution by the molten steel (Fig. 22 and 23): thus the ruin of control rods occurred at 1750 K. But this early degradation has not a strong influence on the plugging of the flow area in the core (16 control rods for 207 fuel rods).

B.4 - Hydrogen mass generation

The hydrogen mass generated by oxidation of zircaloy reaches 130 Kg in the calculation at 174 mm. The change of slope (Fig. 25) shows the loss of oxidation source after melting of zircaloy (Fig. 24).

B.5 - Fission product release

The fission product release calculated in ICARE2 with the CORSOR module after 174 mn in the accident is very small for Tellurium, but for Cesium and Iodine it is more than 20 %.

IV - CONCLUSION

The CSNI exercise on TMI-2 accident was a good test for the assessment of the SFD computation codes.

The most important contribution of the CATHARE1/ICARE1 calculation concerns the thermalhydraulic analysis. The computation is very good before 8340 s (139 mn). The limited use of the standard boundary conditions resulted in a detailed analysis of the initial transient (- 6 s, 480 s) which is the main difficulty from the thermalhydraulic point of view.

The analysis of the phase 2 with this integrated code is obviously restricted by the main features of the preliminary version of CATHARE1 / ICARE1. The main problems are the prediction of the repressurization and the behavior of the pressurizer. In our opinion, the beginning of the repressurization cannot be explained by the hydrogen production.
The CATHARE computations of this study are 'very expensive' because the aim of the code is to do best-estimate analysis (a simulator is developed on the basis of CATHARE); the calculation gives a detailed description of the accident. So it is necessary to compare CATHARE, for the numerical point of view, with the same type of code. This analysis exercise has shown the capability of the CATHARE code to simulate thermalhydraulic transient on an actual reactor scale.

Concerning ICARE2, the physical assumptions of computation lead to collapse before the start of 2B pump at 174 am. This important point needs additional investigations.

The analysis of results showed the urgent necessity to develop a model of oxidation for the molten zircaloy state in the ICARE2 code.
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APPENDIX A

CATHARE1 / ICARE1

CODE DESCRIPTION
APPENDIX A

COMPUTER CODE DESCRIPTION

\[
\text{CATHARE1/ICARE1}
\]

The CATHARE1 code represents a joint effort of the French partners; FRAMATOME, EDF and CEA. CATHARE simulates all the thermalhydraulic phenomena which occur in the primary and secondary circuits during a LOCA ([1], [2], [3]). The two phase - two fluid - six equation model is introduced into all the CATHARE components, there being : termed : pipe, capacity, and "T". They allow for the description of any kind of circuit. Concerning numerical and structural characteristics, the code is designed in four parts:

- the data acquisition which allows to specify any sequence of actions or trips,
- an actual steady state calculation in single-phase flow,
- the transient calculation: A fully implicit scheme in time is used for all the components,
- the post-processing.

CATHARE1/ICARE is a preliminary version of the future integrated code CATHARE2/ICARE2 which will be able to simulate severe accidents up to the vessel failure. In this preliminary version the fuel damage is limited to the clad deformation and upsture and to clad oxydation (URBANIC and HEIDRICK model). The correlation of creep strain rates and stress at rupture are based on the analytical experiments EDGAR (see [4])
APPENDIX B

ICARE

A COMPUTER CODE FOR SEVERE FUEL DAMAGE ANALYSIS

GENERAL DESCRIPTION OF THE SECOND VERSION : ICARE2

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APPENDIX B

ICARE2

I - INTRODUCTION AND CODE DESIGN

1.1 - INTRODUCTION

Since the accident at the TMI-2 reactor in 1979, a world-wide effort has been undertaken for the study of the severe core damage phenomena in LWRs. Numerous experiments /1, 2/ and analytical researches /3, 4/ have been performed in order to get a better understanding of the core-melt processes which govern the fission products release during a severe accident. In France, a Severe Fuel Damage program both experimental (PHEBUS /5/) and analytical (ICARE) is conducted to analyse these phenomena.

ICARE2 modeling, present status and future plans are discussed in this paper.

1.2 - CODE DESIGN

SIGAL

ICARE2 uses a new technical data organisation allowing especially a dynamic management of the computer memory, called SIGAL in french (Système Informatique de Gestion et d’Accueil de Logiciels).

This technique sets up several functions concerning in the same time programmers and users. Each function is associated with either a library or a code:

- dynamic memory management library: it optimizes the use of central computer’s memory.

- memory organisation library (main original feature of SIGAL): it allows to handle and to modify SIGAL data bases stored in the central memory.

- data acquisition program: it reads structured user’s data and stores them in a SIGAL data base.

- data control program: expert system comparing user’s data with a knowledge data base (file created by the programmer and containing the correct data form as well as the associated physical or logical rules they have to respect).

- analyser program: it interprets special user’s data to perform all manipulation of SIGAL data bases.

- T.I.C. (in french Traceur Interactif de Courbes): self-governing graphic program which is able to post-process all FORTRAN 77 code. It can also use the analyser program to extract and draw data stored in a SIGAL data base.

Internal ICARE2 organisation

ICARE2 code is written in a modular structure. The numerical scheme of the main physical modules is as implicit in time as possible, but the coupling between them is explicit. Then the energetic error induced by these couplings controls essentially the global time step management.

II - DESCRIPTION OF ICARE2 MODELING

2.1 - BASIC ASSUMPTION IN MODELING

The main SFD facilities, as the reactor cores, consist of fuel rods, control rods and coolant channel surrounded by a shroud which is cooled by a fluid on the outer side. The phenomena occurring in severe accidents deal with high temperature and thus appear mainly in vapor and/or hydrogen content. To focus on these major phenomena avoiding expensive cost calculations, ICARE2 models the fuel rods by ring representation and the coolant channel by an hydrogen and/or vapor mixture; the whole is enclosed in a multilayer shroud.

2.2 - THERMAL HYDRAULIC MODEL

The model is derived from the 1D two phase flow model of the CATHARE code /7/, without liquid phase available in the present version of ICARE2 code but with some improvements allowing a good study of severe fuel damage experiments. These improvements are summarized below:

- the gas phase flowing into the channel can be either pure vapor, or a mixture of steam and hydrogen. In the
second case, it is assumed that steam and hydrogen have the same temperature and velocity in a given cross section. In addition, the diffusion of the non condensable gas in the steam is taken into account.

- the geometry changes due to the flowdown of melting materials can be correctly treated owing to an independant description of hydraulic volumes and cross sections as well as an evolutive number of convective heat transfer exchanges in a given mesh (appearance or disappearance of materials).

Boundary conditions:

The boundary conditions are generally pressure at the bundle outlet and temperature at the bundle inlet and may be either massic concentration of hydrogen and velocity, or hydrogen and vapor mass flow rates at the entrance.

Constitutive laws:

They are derived from those of CATHARE code, in which physical values of gaseous mixture are used.

Numerics:

The set of 4 equations is approached with finite difference scheme using axial staggered meshes ($P_G, H_G$ and $V_H$ located in a centered mesh cell and $P_G$ at the surface mesh cell) and a fully implicit technique; this discretization leads to a non linear system solved by the NEWTON-RAPHSON iterative method /7/.

2.3 - THERMAL MODELS

The heat and mass transfer between the structures are summarized below:

- the thermal conduction and/or radiation through a gas located between 2 structures, such as gap exchanges in a rod (the gas temperature is imposed to be equal to the half sum of the 2 structure temperatures)

- the radiation heat transfer between the structures of a channel filled with a participative fluid

- the heat and mass transfer exchanges linked with the flowdown and relocation of molten materials

It is necessary to make cles following paragraphs to give details about the energetic balance equation of a structure.

2.3.1 - Energetic balance equation

The intersection between a meshing and a structure (or the fluid) generates structure (or fluid) meshes called "component" in the ICARE2 vocabulary.

A component is characterized by:

- an average temperature,
- a geometry,
- a chemical composition (pure materials and/or mixture of them),
- a total mass.

The heat and mass transfer take place through the component faces whose number depends on its geometry (plane, cylindrical, spherical). The energetic balance equation expresses the total enthalpy conservation in a lumped form:

2.3.2 - Radiation heat transfer

The thermal radiation transfer of ICARE2 is based on the net radiation enclosure model /12/.

Each numerical cell corresponds to a thermalhydraulic mesh. It is considered as a net radiation enclosure with structures and filled by a homogeneous, non scattering, emitting and absorbing medium.

As usually done, structure surfaces are supposed grey, diffuse emitters, absorbers and reflectors.

There is no radiative heat exchange between two net radiation enclosures. The validity of this assumption has already been studied for a bundle, and it was demonstrated that radiative exchanges remain essentially two dimensional /10/. This conclusion allows one to compute the view factors in 2D by the analytical crossed-string method /11/.
2.4 - CORE GEOMETRY CHANGE
(CANDLING)

The first version of ICARE2 deals only with flowdown of molten materials along vertical cylinders such as rods and shroud.

2.4.1 - Preliminary definitions

In the general case, a component can be composed of one or several "elements" which are either pure materials or a mixture of pure materials to simulate alloy compounds. The average properties of a component (an element) is a linear function of its element (its pure material) properties.

2.4.1.1 - Element properties

An element is said a "mixture" if it is composed of:
- more than one pure material,
- only one pure melted or dislocated material.

The liquid mass fraction of a "mixture" element is chosen equal to the greatest one of its pure materials.

When two "mixture" elements are adjacent in a component, they are mixed to form only one "mixture" element.

2.4.2 - Flowdown occurrence

Only a "mixture" element is able to flow-down if some input data conditions are full filled:
- this mixture pertains to a component assigned,
- it is located on the desired component face, and is not enclosed between two other elements (stratification configuration),
- its liquid mass fraction is greater than a specified value.

2.4.3 - Melt progression and relocation

In the present version, some physical parameters can be given as input data:
- the flow-down velocity,
- the wetting surface capability $X_w$ which is a function of the melting properties and the flow regime (film or droplets) (the default value is 1 corresponding to a perfect wetting).
- the refreezing velocity $V_r$ which corresponds to the propagation velocity of the refreezing front in a liquid.

The mass amount $M_e$ of molten materials in contact with a component is coming from a "mixture" element located above it in the bundle.

2.4.4 - Present model limitations

- The flowdown of molten material occurring simultaneously on two neighboring vertical cylinders does not interact one with each other even when contact is geometrically reached. Then no mass transfer between a cylinder to another is available.
- The dislocation of solid materials does not lead to debris bed formation.
- As pointed out previously, the candling velocity is given as constant value in input data; however, a correlation or a momentum equation could be easily implemented if the necessity arose.

2.5 - CHEMICAL INTERACTION

2.5.1 - Zircaloy steam interaction model

The model takes into account the two physical processes governing the oxidation kinetics:
- the steam diffusion within gaseous mixture from bulk to wall that provides the oxygen mass flux available at the wall (blanketing effect)
- the oxygen diffusion within metallurgical layers of the cladding according to the wall boundary conditions resulting from the previous process.

Experimental laws are therefore necessary, giving in a coherent set the variation of the oxide and $\theta$ layer thickness and the total oxygen mass gain. The ICARE2 model provides users with four sets of parabolic laws which are deduced from the works of CATHCART and PAWEL, LEISTIKOV, BIEDERMAN and URBANIC - HEIDRIIC.
C - Energy and hydrogen release:

The energy release associated with the oxidation reaction is corrected by the steam enthalpy difference between wall and steam temperature.

D - Numerics:

The oxidation chemical reactions are implicitly coupled with the thermal hydraulic module to avoid numerical problems in the case of great consumption of steam. The oxygen flux F02U required in unlimited steam conditions by each oxidized structure is known during the time step; the available oxygen flux at the wall F02A depends strongly on the steam mass fraction in the bulk.

In the thermal hydraulic iterative solution, the mass and energy transfer between wall and fluid are computed using an oxygen flux equal to F02A. F02U is a maximum value for F02A, in order to prevent the participation in the reaction of an amount of steam greater than the present one.

Then, when the convergence is assumed in the thermal hydraulic module, the zircaloy oxidation module computes the layers growth on the basis of an available oxygen flux equal to the same value F02A.

2.5.2 - Interaction model of UO2 with solid zircaloy

The contact between the cladding and the fuel, which may occur during high pressure transients, leads to chemical interaction if the contact is good and if the temperature is sufficiently high (greater than 1473K).

The UO2 reacts with zircaloy to form (U, Zr) alloy located between two α - Zr(0) layers.

This chemical reaction is an oxygen diffusion controlled process and obeys parabolic rate laws. Then, the growth rate of metallurgical layers, derived from experimental work of Hofman /14/.

The physical properties of the (U, Zr) layer and its composition as a function of time are unknown. Then, this layer is assumed in ICARE2 to be a mixture of Zirconium and UO2 properties can be easily computed, the mass fraction of UO2 in the mixture is an input data.

This assumption done through lack of physical informations allows nevertheless mass conservation during the chemical reaction. The correlations giving the thickness growth of the layers are applied in conjunction with 2 mass conservation equations, for Zircaloy and UO2.

The reduction of the UO2 by the Zircaloy stops when the prior ZrO of the cladding is consumed by this reaction or by another one.

2.5.3 - Interaction model of UO2 with liquid zircaloy

This interaction has been studied by several authors (/15/, /16/). These experimental works show that this complex process is time dependent; it is a diffusion controlled reaction according to Hofman (parabolic law) /15/ and a convection controlled reaction from Kim and Olander /16/; the reaction surface is vertical.

In the present version of the code, the modelisation of this reaction is simple. The pseudo binary phase diagram of Politis /17/ predicts the total amount of UO2 in the mixture. The dissolution process is not time dependent because an instantaneous equilibrium assumption is made. The solubility limit curve of UO2 in liquid zircaloy can also be given as an input data instead of the Politis one.

In the code, the composition of the (U-Zr-O) alloy simply a mixture of liquid zircaloy with solid particles of UO2. The ratio of UO2 molar fraction in the mixture is given by solubility limit curve.

2.6 - MECHANICAL MODEL

In fact, the present version of ICARE2 has not a mechanic module allowing stress and ballooning calculations.

Nevertheless, thermal expans are taken into account, and a loss of integrity model leads to derive
three possible physical states for a material:
- compact
- fissured
- dislocated

The change from a state to another is at present time imposed through input data criteria that physical parameters attached to the material must verify.

The parameters can be either temperature, or thickness or instantaneous temperature evolution or time.

The input threshold values for these parameters can be either minimum or maximum values. A parameter interferes in the state modification if its threshold value is given in input data.

When a material becomes dislocated, it can be added to a neighbouring material yet composed of a mixture of several pure materials either solid or liquid.

A liquid material can flow down when it is no more enclosed by neighbouring compact materials.

The concept of fissured state acts essentially for internal cladding oxidation by the channel steam and for the fission products release.

2.7 - FISSION PRODUCTS RELEASE MODEL

The CORSOR-M model /18/ was introduced in ICARE2 code to simulate the release of fission products and structural materials from a core containing fuel and control rods. Forty species are involved and two separate release mechanisms are used depending on species and temperature history profile.

Gap release

A small fraction of the volatile fission product species lies in the fuel cladding gap during normal reactor operation and is subject to a one-time release when cladding fissuration occurs. This release mechanism is simulated by releasing the fraction of the inventory of the species in table 2.

- release of refractory fission products and structural materials are controlled by vaporization (so the Q values are heats of vaporization for these species).

- release of Ba, Sr, La are done in oxide form (so the Q values are heats of vaporization of these oxides).

Moreover the interaction of the tellurium with the zircaloy cladding is taken into account by an increase of the Te release if the cladding oxidation exceeds 70 %.
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Fig. 1: CATHARE modelization of the TMI-2 primary circuit and secondary sides of OTSG (not to scale in the horizontal axis)
Fig. 2: Primary pressure comparisons for the initial transient.
Fig 4: Pressurizer collapsed level comparisons.
Fig. 5: A-loop mass flow rate comparisons.
Fig. 6: Predicted cladding temperatures in the central region of the core.
BOUNDARIES CONDITIONS FOR ICARE2 COMPUTATION  FIG. 12
APPENDIX A

CATHARE1 / ICARE1

CODE DESCRIPTION
APPENDIX A

COMPUTER CODE DESCRIPTION

CATHARE1/ICARE1

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APPENDIX B

ICARE

A COMPUTER CODE FOR SEVERE FUEL DAMAGE ANALYSIS

GENERAL DESCRIPTION OF THE SECOND VERSION : ICAREZ

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APPENDIX B
ICARE2

I - INTRODUCTION AND CODE DESIGN

I.1 - INTRODUCTION

Since the accident at the TMI-2 reactor in 1979, a world-wide effort has been undertaken for the study of the severe core damage phenomena in LWRs. Numerous experiments /1, 2/ and analytical researches /3, 4/ have been performed in order to get a better understanding of the core-melt processes which govern the fission products release during a severe accident. In France, a Severe Fuel Damage program both experimental (PHEBUS /5/) and analytical (ICARE) is conducted to analyse these phenomena.

ICARE2 modeling, present status and future plans are discussed in this paper.

I.2 - CODE DESIGN

SIGAL

ICARE2 uses a new technical data organisation allowing especially a dynamic management of the computer memory, called SIGAL in french (Système Informatique de Gestion et d'Accueil de Logiciels).

This technique sets up several functions concerning in the same time programmers and users. Each function is associated with either a library or a code:

- dynamic memory management library : it optimizes the use of central computer's memory.
- memory organisation library (main original feature of SIGAL) : it allows to handle and to modify SIGAL data bases stored in the central memory.
- data acquisition program : it reads structured user's data and stores them in a SIGAL data base.
- data control program : expert system comparing user's data with a knowledge data base (file created by the programmer and containing the correct data form as well as associated physical or logical rules they have to respect).
- analyser program : it interprets special user's data to perform all manipulation of SIGAL data bases.
- T.I.C. (in french Traceur Interactif de Courbes) : self-governing graphic program which is able to post-process all FORTRAN 77 code. It can also use the analyser program to extract and draw data stored in a SIGAL data base.

Internal ICARE2 organisation

ICARE2 code is written in a modular structure. The numerical scheme of the main physical modules is as implicit in time as possible, but the coupling between them is explicit. Then the energetic error induced by these couplings controls essentially the global time step management.

II - DESCRIPTION OF ICARE2 MODELING

2.1 - BASIC ASSUMPTION IN MODELS

The main SFD facilities, as the reactor cores, consist of fuel rods, control rods and coolant channel surrounded by a shroud which is cooled by a fluid on the outer side. The phenomena occurring in severe accidents deal with high temperature and thus appear mainly in vapor and/or hydrogen content. To focus on these major phenomena avoiding expensive cost calculations, ICARE2 models the fuel rods by ring representation and the coolant channel by an hydrogen and/or vapor mixture ; the whole is enclosed in a multilayer shroud.

2.2 - THERMAL HYDRAULIC MODEL

The model is derived from the 1D two phase flow model of the CATHARE code /7/. without liquid phase available in the present version of ICARE2 code but with some improvements allowing a good study of severe fuel damage experiments. These improvements are summarized below:

- the gas phase flowing in channel can be either pure vapor, a mixture of steam and hydrogen. In the
second case, it is assumed that steam and hydrogen have the same temperature and velocity in a given cross section. In addition, the diffusion of the non-condensible gas in the steam is taken into account.

- the geometry changes due to the flowdown of melting materials can be correctly treated owing to an independent description of hydraulic volumes and cross sections as well as an evolutive number of convective heat transfer exchanges in a given mesh (appearance or disappearance of materials).

**Boundary conditions:**

The boundary conditions are generally pressure at the bundle outlet and temperature at the bundle inlet and may be either massic concentration of hydrogen and velocity, or hydrogen and vapor mass flow rates at the entrance.

**Constitutive laws:**

They are derived from those of CATHARE1 code, in which physical values of gaseous mixture are used.

**Numerics:**

The set of 4 equations is approached with finite difference scheme using axial staggered meshes (Pc, Hc and Vc at the center mesh cell and Vc at the surface mesh cell) and a fully implicit technique ; this discretization leads to a non linear system solved by the NEWTON-RAPHSON iterative method /7/.

### 2.3.1 - Energetic balance equation

It is necessary to make clear the following paragraphs to give some details about the energetic balance equation of a structure.

**2.3.2 - Radiation heat transfer**

The thermal radiation heat transfer of ICARE2 is based on the net radiation enclosure model /12/.

Each numerical cell corresponds to a thermalhydraulic mesh. It is considered as a net radiation enclosure with structures and filled by a homogeneous, non-scattering, emitting and absorbing medium.

As usually done, structure surfaces are supposed grey, diffuse emitters, absorbers and reflectors.

There is no radiative heat transfer exchange between two net radiation enclosures. The validity of this assumption has already been studied for a bundle, and it was demonstrated that radiative exchanges remain essentially two dimensional /10/. This conclusion allows one to compute the view factors in 2D by the analytical crossed-string method /11/.
2.4 - CORE GEOMETRY CHANGE (CANDLEING)

The first version of ICARE2 deals only with flowdown of molten materials along vertical cylinders such as rods and shroud.

2.4.1 - Preliminary definitions

In the general case, a component can be composed of one or several "elements" which are either pure materials or a mixture of pure materials to simulate alloy compounds. The average properties of a component (an element) is a linear function of its element (its pure material) properties.

2.4.1 - Element properties

An element is said a "mixture" if it is composed of:
- more than one pure material,
- only one pure melted or dislocated material.

The liquid mass fraction of a "mixture" element is chosen equal to the greatest one of its pure materials.

When two "mixture" elements are adjacent in a component, they are mixed to form only one "mixture" element.

2.4.2 - Flowdown occurrence

Only a "mixture" element is able to flow-down if some input data conditions are full filled:
- this mixture pertains to a component assigned,
- it is located on the desired component face, and is not enclosed between two other elements (stratification configuration),
- its liquid mass fraction is greater than a specified value.

2.4.3 - Melt progression and relocation

In the present version, some physical parameters can be given as input data:
- the flow-down velocity,
- the wetting surface capability Xw which is a function of the melting properties and the flow regime (film droplets) (the default value is 1 corresponding to a perfect wetting).
- the refreezing velocity Vr which corresponds to the propagation velocity of the refreezing front in a liquid.

The mass amount Me of molten materials in contact with a components is coming from a "mixture" element located above it in the bundle.

2.4.4 - Present model limitations

- The flowdown of molten material occurring simultaneously on two neighbouring vertical cylinders does not interact one with each other even when contact is geometrically reached. Then no mass transfer between a cylinder to another is available.
- The dislocation of solid materials does not led to debris bed formation.
- As pointed out previously, the candling velocity is given as constant value in input data; however correlation or a momentum effect could be easily implemented if the necessity arose.

2.5 - CHEMICAL INTERACTION

2.5.1 - Zircaloy steam interaction model

The model takes into account the two physical processes governing the oxidation kinetics:
- the steam diffusion within gaseous mixture from bulk to wall that provides the oxygen mass flux available at the wall (blanketing effect)
- the oxygen diffusion within metallurgical layers of the cladding according to the wall boundary conditions resulting from the previous process.

Experimental laws are therefore necessary, giving in a coherent set the variation of the oxide and ZrO2 layer thickness and the total oxygen mass gain. The ICARE2 model provides users with four sets of parabolic laws which are deduced from the works of CATP and PAVEK, LEISTIKOV, BIEDERMAN URBANIC - HEIDRICK.
C - Energy and hydrogen release:

The energy release associated with the oxidation reaction is corrected by the steam enthalpy difference between wall and steam temperature.

D - Numerics:

The oxidation chemical reactions are implicitly coupled with the thermal hydraulic module to avoid numerical problems in the case of great consumption of steam. The oxygen flux FO2U required in unlimited steam conditions by each oxidized structure is known during the time step; the available oxygen flux at the wall FO2A depends strongly on the steam mass fraction in the bulk.

In the thermal-hydraulic iterative solution, the mass and energy transfer between wall and fluid are computed using an oxygen flux equal to FO2A. FO2U is a maximum value for FO2A, in order to prevent the participation in the reaction of an amount of steam greater than the present one.

Then, when the convergence is assumed in the thermal-hydraulic module, the zircaloy oxidation module computes the layers growth on the basis of an available oxygen flux equal to the same value FO2A.

2.5.2 - Interaction model of UO2 with solid zircaloy

The contact between the cladding and the fuel, which may occur during high pressure transients, leads to chemical interaction if the contact is good and if the temperature is sufficiently high (greater than 1473K).

The UO2 reacts with zircaloy to form (U, Zr) alloy located between two α - Zr(0) layers.

This chemical reaction is an oxygen diffusion controlled process and obeys parabolic rate laws. Then, the growth rate of metallurgical layers, derived from experimental work of Hofman /14/.

The physical properties of the (U, Zr) layer and its composition as a function of time are unknown. Then, this layer is assumed in ICARE2 to be a mixture of zirconium and UO2 whose properties can be easily computed. The mass fraction of UO2 in the mixture is an input data.

This assumption done through lack of physical informations allows nevertheless mass conservation during the chemical reaction. The 3 correlations giving the thickness growth of the layers are applied in conjunction with 2 mass conservation equations, for Zircaloy and UO2.

The reduction of the UO2 by the Zircaloy stops when the prior Zr8 of the cladding is consumed by this reaction or by another one.

2.5.3 - Interaction model of UO2 with liquid zircaloy

This interaction has been studied by several authors (/15/, /16/). These experimental works show that this complex process is time dependent: it is a diffusion controlled reaction according to Hofman (parabolic law) /15/ and a convection controlled reaction from Kim and Olander /16/ when the reaction surface is vertical.

In the present version of the code, the modelisation of this reaction is simple. The pseudo binary phase diagram of Politis /17/ predicts the total amount of UO2 in the mixture. The dissolution process is not time dependent because an instantaneous equilibrium assumption is made. The solubility limit curve of UO2 in liquid zircaloy can also be given as an input data instead of the Politis one.

In the code, the composition of the (U-Zr-0) alloy simply a mixture of liquid zircaloy with solid particles of UO2. The ratio of UO2 molar fraction in the mixture is given by solubility limit curve.

2.6 - MECHANICAL MODEL

In fact, the present version of ICARE2 has not a mechanic module allowing stress and ballooning calculations.

Nevertheless, thermal expansions are taken into account, and a simple loss of integrity model leads to define
three possible physical states for a material:

- compact
- fissured
- dislocated

The change from a state to another is at present time imposed through input data criteria that physical parameters attached to the material must verify.

The parameters can be either temperature, or thickness or instantaneous temperature evolution or time.

The input threshold values for these parameters can be either minimum or maximum values. A parameter interferes in the state modification if its threshold value is given in input data.

When a material becomes dislocated, it can be added to a neighbouring material yet composed of a mixture of several pure materials either solid or liquid.

A liquid material can flow down when it is no more enclosed by neighbouring compact materials.

The concept of fissured state acts essentially for internal cladding oxidation by the channel steam and for the fission products release.

2.7 - FISSION PRODUCTS RELEASE MODEL

The CORSOR-M model /18/ was introduced in ICARE2 code to simulate the release of fission products and structural materials from a core containing fuel and control rods. Forty species are involved and two separate release mechanisms are used depending on species and temperature history profile.

Gap release

A small fraction of the volatile fission product species lies in the fuel cladding gap during normal reactor operation and is subject to a one-time release when cladding fissuration occurs. This release mechanism is simulated by releasing the fraction of the inventory of the species in table 2.

- release of refractory fission products and structural materials are controlled by vaporization (so the Q values are heats of vaporization for these species).

- release of Ba, Sr, La are done in oxide form (so the Q values are heats of vaporization of these oxides).

Moreover the interaction of the tellurium with the zircaloy cladding is taken into account by an increase of the Te release if the cladding oxidation exceeds 70 %.
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FINAL CSNI TMI-2 ANALYSIS EXERCISE REPORT

FRENCH PARTICIPATION

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I - INTRODUCTION

In the frame of the CNSI TMI-2 benchmark exercise, we present the French contribution under the form of a synthesis of the most significant results of the calculation performed with CATHARE1/ICARE1 for the phase 1 and the beginning of phase 2 up to important material relocation and with ICARE2 for the phase 2. The main features of those codes are described in appendix.

The CATHARE1/ICARE1 computation results have been presented to the October 1988 CNSI TMI-2 meeting, and the ICARE2 computation results to the October 1989 meeting.

This CNSI exercise is very important because it is an opportunity of to emphasis exertion about of phenomena modeling and speeding up the development of codes able to take into account these phenomena in accident sequences.

II - RESULTS

II.1 - LOCA PHASE RESULTS
(CATHARE1/ICARE1 COMPUTATION)

A - Node diagram

The CATHARE model of the TMI-2 reactor takes into account the primary circuit and secondary side of the once-through-steam-generators (OTSG). Therefore three independent circuits are modeled. A schema is given fig. 1.

* The CATHARE components used are:
  - pipe : downcomer, core bypass, core, hot and cold legs, primary sides of OTSG, surge line, "PORV line", line between pressurizer and pilot-operated-relief-valve (PORV), upper and lower boilers of OTSG.
  - volume : upper downcomer, lower plenum, upper plenum, upper head, pressurizer, water boxes, OTG downcomers.
  - "T" : surge line junction, OTSG aspirators (bypass steam for feedwater heating).

* Each hydraulic component is associated with at least one structure.

It is assumed that there is no loss to the containment.

The core is connected to four structures:

- 3 average rods with ten axial nodes
  - 1 for the central region (5 200 rods)
  - 1 for the intermediate region (14 976 rods)
  - 1 for the outer region (16 640 rods)

- 1 structure for the core baffle.

We don't take into account the control rods and the grid spacers.

* the other main assumptions are:
  - the two cold legs per loops are modeled by one pipe,
  - the thermal coupling between the primary side and the secondary side of the OTSG is explicit (it is an exception to the fully implicit rule of CATHARE),
  - the flow area of the vent is proportional to the difference pressure between the upper plenum and the downcomer.

* The boundary conditions of the computation are:

  - the PORV flow rate: the code computes this flow rate. The method is based on the calculation of the system's characteristics (more specifically stated, this is the system coming from the six equation model). A singular pressure drop upstream the PORV has been adjusted in order to make the calculated flow fit the experimental flow rate: 16.4 kg/s at 15.65 MPa (analysis exercise data, revision 4).

  - The feedwater of steam generators:
    - normal: measured
    - auxiliary: estimated or computed.

These unknown boundary condition is controlled in order to follow the recorded OTSG levels (proportional Dif-
This flowrate is injected in the following distribution:

65 % injected at the bottom of the boiler
25 % injected at the middle of the boiler
10 % injected at the top of the boiler

In phase 2, we change the position of lower injection so that it is above the liquid level.

- The outlet pressure of the steam generators: measured.
- The letdown flow, the high pressure injection, the decay heat and the power of the pressurizer heaters: analysis exercise data.

- The momentum and the energy due to the primary pumps: the pump data given by INEL are related to the first quadrant in single-phase flow. For the other quadrants and for two-phase flow degradation, we have used the homologous curves of the LOFT facility pumps (because in the first quadrant, its behavior is very similar to the TMI-2 pumps. The motor torque is approximated by:

\[ C_m = \frac{C_{mn} g (a + b |g|)}{(cg)^2 + (a + b |g|)^2} \]

with:
- \( g \): slip
- \( C_{mn} = 43170 \) m·N: Nominal motor torque
- \( a = 0.007644 \)
- \( b = 0.045406 \)
- \( c = 0.189626 \)

B - Discussion of results

In the analysis exercise, the turbine trip has been chosen as time 0 of the accident. But the transient begins before this instant: the last plant computer record of the initial steady state is -6 s. Our initial steady state computation is related to this instant. It is performed in two steps:

- calculation of the two-phase flow steady state in the secondary side of OTSG (CATHARE 1 steady state calculation option runs in single phase flow).
- initialization of the full CATHARE model with the previous results.

The phase 1 of the accident is well simulated by CATHARE. Primary pressure (fig. 2 and fig. 3), pressurizer level (fig. 4), and RCS mass flow rates (fig. 5) agree well with the TMI-2 data.

* From minus 6 s to 8 s.

The transient begins with the increase of secondary steam pressure, about 1 MPa in 6 seconds (probable consequence of the operations on the secondary circuit). So in the boilers, the two-phase velocities decrease. In consequence, the primary to secondary heat transfer decreases (same result, if the OTSG feedwater flows are not stopped), and the primary pressure begins to increase. Shutdown of the steam generator feedwater accelerates the degradation of the heat transfer in the steam generator. So the primary pressure continues to increase up to the reactor scram (8 s).

* From 8 s to 480 s.

After reactor scram, the primary pressure and the pressurizer level decrease (liquid contraction). Our computation underestimates this pressure (fig. 2). It is a consequence of the overestimation of primary to secondary heat transfer. The dryouts of OTSG occur at 100 s in the computation and at 60 s in the accident. We think that it is due to a "bad" initial water inventory of OTSG: the liquid in the OTSG downcomer is subcooled (≈10°C) because the computed aspirator flow is too small (about 70 kg/s). In the plant, the OTSG downcomer is close to saturation which requires an aspirator flow of 90 kg/s.

After the dryouts of OTSG, the primary pressure is controlled by the steam volume at the top of the pressurizer (balance between the increase of the pressurizer level and the PORV flow rate).
The primary temperatures are constant after 100 s and up to 200 s in the calculation. 270 s in the accident. In the calculation, the instant 200 s is the shutdown of HPI (high pressure injection). So we don't understand why the temperatures don't increase in the accident. The probable explanation is that the HPI continues after 200 s (flywheel of the HPI pumps?).

When the pressurizer is filled with liquid (∼ 340s) the primary pressure again increases. The thermal expansion of the primary liquid can't be absorbed by the pressurizer (the PORV flow rate is too small). This increase of primary pressure is ended when the auxiliary feedwater flows are available (480 s). The fig. 7 shows the void fraction in the circuits just before 480 s : (stratification in the upper head).

* From 480 s to 6040 s.

After a large decrease (up to 1500 s), the primary pressure tends to be stationary (about 7 MPa) and follows the secondary pressures. This 'equilibrium' results from the balance between decay heat, steam generator power, PORV, make-up and letdown flows. During this phase, it is very important to correctly take into account the OTSG secondary levels because they control the primary to secondary heat transfer. This is the case in our study.

After the shutdowns of B loop pumps (74 mn), the A loop controls the transient. The only function of the B loop is related to the make-up flow: this water is streaming to the vessel downcomer. At 6040 s the vapor fraction in the core is about 0.6, but the core is still being cooled (see fig. 9), just before the shutdown of the A pumps, we can notice the stratification in the B loop.

* From 6040 s to 8340 s:

After shutdown of the A loop pumps, the A loop continues to control the transient because a vapor circulation is generated by the steam condensation in the primary side of the OTSG-A. This condensation is due to a quick increase of the OTSG secondary level up to 7500s and also due to the let down located at the bottom of cold legs, which reduces the OTSG primary level.

In the core, the liquid tends toward stratification so the liquid fraction and steam production increase (by a factor 2) which slows down the primary pressure decrease. We notice that the shutdown of the A pumps has not changed the heat transfer rate in the OTSG-A.

The pressurizer level decreases because there is no more liquid in the hot leg A therefore the vapor which is generated by the pressurizer heaters isn't compensated. The good agreement existing between computed and measured pressurizer levels, see fig. 4, is a consequence of a correct pressurizer heater power).

The core uncovering begins after 6500 s (fig. 6 and fig. 11), the clad temperature increase rates are around 0.5°C/s at this time.

In the accident the decrease of primary pressure is ended at about 8340 s. In the basic computation, the primary pressure continues to decrease and follows the secondary pressure of the OTSG-A.

This point has been studied in sensitivity calculations. This study shows that at 7700 s the repressurization is probably due to the difference between primary and secondary levels in OTSG-A, the effect of hydrogen occurs probably later in the accident so we suspect the "standard letdown flow" (analysis exercise boundary condition) which controls the primary level in OTSG-A.

The following table gives results for clad deformations and failures. Some comments must be done:
In order to compute the clad failure, we had to cancel the feedback of clad deformation on the gap pressure of the rod. This problem is a consequence of the large meshes (2 feet) in the region of clad rupture. A good prediction of this feedback requires very small meshes. That has been done in a separated single rod computation with the thermalhydraulic conditions given by CATHARE. The results of the previous table have been confirmed.

The computed strain at rupture is not compatible with the pitch of the fuel assembly (the code doesn't take into account rod to rod contacts) but compatible with the assembly area. So we can suspect a large flow blockage.

Times of rupture seem to be correct because the first alarm of reactivity in the containment occurred at 8064 s. So we can conclude that the clad temperatures are well predicted up to 8340 s (closure of the PORV valve).

* From 8340 s to 9160 s.

The main problem after the closure of the PORV block valve is the behavior of the pressurizer (see fig. 10, at 8760 s). In our first computations, the liquid which filled the pressurizer (= 30 m³) flowed out of this volume and rewetted the core. This problem is related to the gas velocity at the junction between the surge line and the pressurizer. Before 8340 s, the vapor velocity (= 5 m/s) in the surge line is sufficient to hold up the liquid in the pressurizer. After 8340 s, this velocity is too low and lets the liquid fall down. In the condition of the calculation, a gas velocity about 0.8 m/s is required by CATHARE to hold the liquid up. After 8340 s, such a velocity requires an increase in the primary pressure (but it is not a sufficient condition). This is not the case in the basic computation (with NMI analysis exercise boundary conditions and geometry). Therefore, in order to calculate the overheating of the core in the basic computation, we have decided to close the junction between the surge line and the pressurizer after 8340 s.

Concerning the fuel behavior, the main event is the oxidation escalation at 9145 s which results in the clad temperature : 1580°C. This change of the oxidation kinetics could explain the increase of the primary pressure observed at about 9300 s. The melting temperature of the zircaloy is reached at 9160 s and we stop this basic computation when the zircaloy is completely molten at a given axial mesh.

II.2 - SFD PHASE RESULTS (ICARE2 CODE)

A - Node diagram

The data needed to perform an ICARE2 calculation can be separated into four sections according to:

A.1 - General description

6 in core
1 baffie plate
1 core barrel
1 vessel
Structures taken into account:
- baffle plate
- core barrel
- neutronic shroud
- vessel
- thermal shroud

A-2 - Physical assumptions of computation
- radiative heat transfer: NONABSORBANT GAS
- chemical interactions:
  - (H2O/solid Zr): URBANIC
  - (UO2/solid Zr): HOFFMANN 87
  - (UO2/liquid Zr): POLITIS
- break criteria
- cladding: Zr ALPHA MELTING
- temperature: 2250 K
- control rod guide tube: 1750 K
- fuel loss of integrity: 2250 K
- fall down velocity of fuel debris: 1 CM/SEC
- flow down velocity of molten materials: 60 CM/SEC

A-3 - Axial nodalisation (Fig. 12)
- Total number of meshes: 20
  - lower part of core: 1 coarse mesh (including the water level after 140 mm) (Fig. 12)
  - medium part of core: 10 small meshes (including the materials relocations after 160 mm).

A-4 - Boundary conditions
For convenience, the starting point of the SFD phase will be defined as the beginning of core uncovering that occurs at about 107 mm in the timing of the accident:

< starting time of computation ICARE2: 107 mm >

Four physical parameters must be provided to the code as data:
- water level decrease,
- steam pressure behavior in the core,
- steam mass flow rate,
- nuclear residual power decrease.

There are large uncertainties on the estimation of water level and steam flow rate which have been given by CATHARE.

B - Discussions of results
B-1 - Fuel rod heat up before core collapse

The main consequence of the core uncovering is the rapid fuel rod heat up; in fact, before the core collapse (up to 3000 s after the beginning of uncovering) the cladding temperature reaches 2250 K (Fig. 14). During the strong oxidation phase, the cladding heat up rate in the core central region reaches 5.7 K/s; nevertheless the cladding oxidised thickness ratio remained less than 20%. When cladding temperature reached 225 (melting temperature of zircaloy alpha phase), the calculation oxidation stopped because the present version of ICARE2 does not take into account this chemical phenomenon on molten zircaloy. This fact explains the change of slope (to 140 mm on clad n°1) seen on the temperature behavior (Fig. 15).

In the same time as the cladding becomes oxidized on its outer side, the fuel undergoes a physical chemical attack by the zircaloy on the inner side of cladding, but UZr alloy production is not significant.

In connexion with the previous phenomena, the fluid exhibited the same heatup rate as the fuel rods except for the sixth subchannel of the core. This fact is corroborated by the examination of the cladding radial temperature profile (Fig. 16) and the axial temperature profile in vessel (Fig. 17). In this case the baffle plate temperature remained under 700 K although the core central temperature in 1200 K. The gap between these temperatures (500 K) is result...
of the quantity of energy caught by the external core channels (sixth and seventh subchannel in the radial meshing in ICARE2 computation).

In the core just above the water level, the local rod temperature is saturated. On the other hand, at the top of the core, the cladding reaches zircaloy melting temperature. Consequently the axial temperature profile in the core (Fig. 17) is very steep.

B-2 - Fuel behavior during core slumping

For the calculation, the basic assumption done is:

The predicted zirconia crust generated by the oxidation of cladding is not thick enough to maintain the stack of fuel pellets when zircaloy reaches its melting temperature.

The immediate consequence is the collapse of fuel when zircaloy starts to melt. At this time (140 mm) the core ruin is effective. Few minutes later, the axial profile (Fig. 18) showed a stabilised temperature between 2.8 meters and 4 meters due to meltdown zircaloy. This last core state is showed by the view (Fig. 19).

Before continuing the analysis of computational results, it is necessary to examine the zircaloy meltdown velocity : VFLD and the fuel slumping velocity : VS. The choice of the value VFLD = 0.6 M/s was made after a sensitivity study which showed that this parameter was not important to the core ruin evolution. It seems that the slumping velocity (VS = 0.01 M/s) is not a very sensitive parameter too.

During the core slumping (Fig. 20, Fig. 21) it is seen that the resulting debris bed is very compact : this unrealistic prediction is due to the fact that in present version of ICARE2, it is not possible to account for bed porosity : its modelling is foreseen.

B-3 - Control rods behavior core degradation

During core degradation the control rods meltdown occurred early because the melting temperature of silver-cadmium-indium (AlC) alloy is about 1220 K (the boron carbide control rods are not modelled in the calculation). At the steel melting temperature the guide tube lost its integrity (hypothesis of zircaloy dissolusion by the molten steel) (Fig. 22 and 23): thus the ruin of control rods occurred at 1750 K. But this early degradation has not a strong influence on the plugging of the flow area in the core (16 control rods for 207 fuel rods).

B-4 - Hydrogen mass generation

The hydrogen mass generated by oxidation of zircaloy reaches 130 Kg in the calculation at 174 mn. The change of slope (Fig. 25) shows the loss of oxidation source after melting of zircaloy (Fig. 24).

B-5 - Fission product release

The fission product release calculated in ICARE2 with the CORSOR module after 174 mn in the accident is very small for Tellurium, but for Cesium and Iodine it is more than 20 %.

IV - CONCLUSION

The CSNI exercise on TMI-2 accident was a good test for the assessment of the SFD computation codes.

The most important contribution of the CATHARE/ICARE2 calculation concerns the thermal hydraulic analysis. The computation is very good before 8340 s (139 mn). The limited use of the standard boundary conditions resulted in a detailed analysis of the initial transient (- 6 s, 480 s) which is the main difficulty from the thermal hydraulic point of view.

The analysis of the phase 2 with this integrated code is obviously restricted by the main features of the preliminary version of CATHARE / ICARE. The main problems are the prediction of the repressurization and the behavior of the pressurizer. In our opinion, the beginning of the repressurization cannot be explained by the hydrogen production.
The CATHARE computations of this study are "very expensive" because the aim of the code is to do best-estimate analysis (a simulator is developed on the basis of CATHARE); the calculation gives a detailed description of the accident. So it is necessary to compare CATHARE, for the numerical point of view, with the same type of code. This analysis exercise has shown the capability of the CATHARE code to simulate thermalhydraulic transient on an actual reactor scale.

Concerning ICARE2, the physical assumptions of computation lead to collapse before the start of ZB pump at 174 mn. This important point needs additional investigations.

The analysis of results showed the urgent necessity to develop a model of oxidation for the molten zircaloy state in the ICARE2 code.
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(LOCA PHASE)

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/3/ "The French Thermal-Hydraulic Program Addressing the Requirements of Future Pressurized Water Reactors"
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/4/ "EDGAR program, LOCA type single rod first tests"
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OECD/CSNI Specialist meeting on light water reactor fuel behavior
September 11-12 1986 - CADARACHE FRANCE
Fig. 1: CATHARE models of the TMI-2 primary circuit and secondary sides of OTSG. (in scale in the horizontal axis)
Fig. 2: Primary pressure comparisons for the initial transient.
Fig. 3: Primary pressure comparisons.
Fig. 5: A-loop mass flow rate comparisons.
Fig. 6: Predicted cladding temperatures in the central region of the core.
Appendix D

Catedra De Tecnología Nuclear E. T. S. Ingenieros Industriales Universidad Politécnica De Madrid
MARCH3/CTNI CODE CALCULATIONS FOR THE TMI-2 ANALYSIS EXERCISE

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Madrid, November 1989

1 This work is sponsored by the CSN (Nuclear Safety Council of Spain) and UNESA (Unidad Eléctrica, SA).
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1. - INTRODUCTION.

The TMI-2 accident is presently being re-analyzed and simulated with severe accident computer codes, as the TMI-2 Analysis Exercise proposed by INEL. On its side, the OECD/CSNI has organized the European contribution to this exercise, where the Chair of Nuclear Technology (CTN) is participating. This work is sponsored by the CSN (Nuclear Safety Council of Spain) and UNESA (Unidad Electrifica, SA).

The purpose of this Analysis Exercise is to evaluate the code capabilities and eventually to benchmark the severe accident codes on the basis of the TMI-2 experience. As an additional product, several codes upgrades, and development of new models, have been achieved by several organizations, as is the case of the Chair of Nuclear Technology.

Due to practical reasons, the accidental sequence has been splitted in four phases, from 1 to 100, 100 to 174, 174 to 200 and 200 to 300 minutes, chosen in order to separate different phenomena in different phases. The Spanish participants decided to perform calculations for all the four phases, not only as a contribution to the exercise, but also as an opportunity to gain experience about severe accident phenomena modelling, to evaluate the capabilities of their available computer tools and also to upgrade them.

This report presents some of the results obtained for the TMI standard problem in the Chair of Nuclear Technology.

2. - CODE DESCRIPTION.

The calculations reported herein have been performed with the so-called MARCH3/CTN1 code, which is an updated version of the MARCH3(STCP) code V192, developed at the Chair of Nuclear Technology.

The MARCH3 code has been developed to analyze the thermal-hydraulic response of the reactor core, the primary coolant system and the containment
system for light water reactors in response to accidents involving some level of engineered safety feature inoperability. While MARCH3 is primarily intended to address accidents leading to complete core meltdown, it can also be used to treat events involving only partial core degradation, as well as to assess the minimum levels of engineered safety feature operability required to cope with various accident events. As part of the NRC's Source Term Code Package (STCP), MARCH3 is designed to cover the entire accident sequence, from the initiating accident event through the attack of the containment basement for a variety of accident initiators and including coverage of a wide variety of reactor system designs.

The modeling approach is to treat well understood phenomena realistically on a level of sophistication consistent with the needs. For phenomena which are not well understood, there are a number of user-specified options in the code, that may be selected to explore the effect of various modeling assumptions.

An overall description of the MARCH3(STCP) code can be found in reference /1/, and a very detailed description of the MARCH models, in reference /2/.

The original MARCH3 code from the STCP has been modified at the Chair of Nuclear Technology, with two different objectives:

- to make the code more flexible with respect to their input possibilities.

- to make some of the code models more "realistic", either by suppressing or modifying some unphysical models, or by adding some new ones not available before.

All the modifications have been performed keeping on mind the general use that is devoted at present to MARCH3. This means that they are also applicable to any of the LWR plants which MARCH3 addresses, and not specifically to the Babcock and Wilcox design plant like TMI-2. TMI-2 evidence is the basis where this changes have been made, and against which the new
models have been checked.

The new models incorporated are reported in reference /7/ and they provide a more consistent mass inventory balance, together with two new meltdown models that are able to treat in-core relocation of core material, being one of them for an "homogeneous" core, and the other that can consider different core materials (control alloy, stainless steel, metallic zircalloy and uranium and zirconium dioxide). Some improvements in the steam generator model were also performed.

3.- MODEL DESCRIPTION.

The input model constructed to perform the exercise was made from the data supplied by the Data Bases, references /3/, /4/ and /5/. The initial and boundary conditions used in these calculations are, therefore, the standard conditions provided by EG&G.

3.1.- WATER MASS BALANCE.

The initial water inventories were obtained from the data base, and are represented in the following table.

<table>
<thead>
<tr>
<th>TABLE 1.- Initial Water Inventories</th>
</tr>
</thead>
<tbody>
<tr>
<td>Total primary system volume.........332.3 m³</td>
</tr>
<tr>
<td>Total steam volume..................19.8 m³</td>
</tr>
<tr>
<td>Total primary system water..........219710 kg</td>
</tr>
<tr>
<td>Lower head water (WATBH1)..........13640 kg</td>
</tr>
<tr>
<td>Low points water (WDED).............74857 kg</td>
</tr>
</tbody>
</table>

The two volumes model that simulates the primary system in the MARCH3/CTN1 version has been used. This means that the water from the ECC is added to the main (vessel) volume, and also the PORV leak is taken from it.
The letdown flow is taken from the dead volume (VDED), where the condensate that comes from the steam generator is also dropped.

It was also decided to model the leak rate throughout the PORV in close agreement with the data base estimation. This objective faces the limitation of the model capabilities of MARCH code, that considers the leak flow to be single phase (steam or liquid water, depending on the valve and liquid level location). Therefore, it was decided to change the relief valve area and location during the accidental sequence, in order to obtain a good approximation to the leak rates from the data base.

First of all, an initial leak rate was modelled adjusting the PORV area to obtain a steam critical flow of 2.585 Kg/s. This was done to simulate the really existing initial leak. With this initial leak present, the valve model is allowed to open the valve at its nominal pressure setpoint. From this time on, the valve area and elevation have been manually adjusted in different timesteps, to match the data from the data base.

![Diagram](image)

Figure 1. - PORV flow rate (model 3).
The modelled flow rate through the PORV is represented in figure 1. A very close agreement with the data base curve has been achieved.

With all these assumptions, and using the meltdown model E, the water mass balance in the primary system is maintained in very close agreement with the data base, and no mass imbalances are therefore produced during the base case simulation.

For the cases where other meltdown models are used, this is not the case for the long term (Phases 3 and 4), due to the fact that a different oxidation rate causes a different pressurization behaviour (due to the presence of hydrogen), and therefore a different PORV leak. However, as the main core degradation phenomena is that occurring during Phase 2, a comparison between the different meltdown models can be made, as it is done in the next chapter.

3.2.- STEAM GENERATOR.

Only one steam generator is modelled in the MARCH code, and the pressure behaviour of the "A" steam generator secondary side was chosen as representative; therefore, it was modelled as constant at 6.895 MPa until 90 minutes into the accident, being reduced linearly to 0.6895 MPa between 90 and 200 minutes, and maintained at this value until the end of the simulation.

The initial water inventory of the secondary side of the steam generator, was modelled to be 23321 kg.

The auxiliary feedwater table used was obtained as the sum of the feedwater rates for the two TMI steam generators from the data base. This table was simplified and is represented in table 2.

Note that an initial flow rate, relatively high, has been simulated for a short period of time, in order to model a certain rundown of the feedwater pumps.
TABLE 2.- Auxiliary Feedwater

<table>
<thead>
<tr>
<th>Time (min)</th>
<th>Flow rate (Kg/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>563.0</td>
</tr>
<tr>
<td>0.006</td>
<td>0.0</td>
</tr>
<tr>
<td>8.9</td>
<td>52.8</td>
</tr>
<tr>
<td>29.25</td>
<td>9.44</td>
</tr>
<tr>
<td>84.7</td>
<td>0.0</td>
</tr>
<tr>
<td>91.65</td>
<td>18.4</td>
</tr>
<tr>
<td>100.0</td>
<td>22.1</td>
</tr>
<tr>
<td>124.6</td>
<td>0.1</td>
</tr>
<tr>
<td>129.3</td>
<td>5.51</td>
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<tr>
<td>134.5</td>
<td>0.095</td>
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<tr>
<td>144.8</td>
<td>11.6</td>
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<tr>
<td>147.4</td>
<td>0.096</td>
</tr>
<tr>
<td>152.3</td>
<td>23.0</td>
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<tr>
<td>174.0</td>
<td>3.3</td>
</tr>
<tr>
<td>216.0</td>
<td>10.92</td>
</tr>
<tr>
<td>232.0</td>
<td>0.5</td>
</tr>
<tr>
<td>300.0</td>
<td>0.5</td>
</tr>
</tbody>
</table>

3.3.- CORE POWER.

For the power evolution of the reactor, the first eight seconds at initial power (2700 MW) were considered; afterwards the tabular decay power curve from the TMI-2 Data Base was chosen. The initial fission product inventory was assumed for an 87 days full power period, from reference /6/.

The core was modelled with 10 radial zones and 21 axial zones, and the peaking factors for these zones were obtained by a normalized average of the data available in the data base, based on the shape of the half-height profile and the half-radius profile, by means of a proper iterative process developed "ad- hoc". This was necessary due to the fact that the data available provided the combined peaking factors (Pr x Pz) and the MARCH input required separated
peaking factor for the radial and axial directions. The factors obtained were:

<table>
<thead>
<tr>
<th>Radial Zone</th>
<th>Number of Bundles</th>
<th>Peaking Factor</th>
<th>Axial Zones*</th>
<th>Peaking Factor</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1</td>
<td>1.4706</td>
<td>1.2,3</td>
<td>0.5502</td>
</tr>
<tr>
<td>2</td>
<td>4</td>
<td>1.2897</td>
<td>4.5,6</td>
<td>1.2025</td>
</tr>
<tr>
<td>3</td>
<td>8</td>
<td>1.1481</td>
<td>7.8,9</td>
<td>1.3283</td>
</tr>
<tr>
<td>4</td>
<td>12</td>
<td>1.1639</td>
<td>10.11,12</td>
<td>1.2072</td>
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<td>5</td>
<td>16</td>
<td>1.0086</td>
<td>13.14,15</td>
<td>1.1751</td>
</tr>
<tr>
<td>6</td>
<td>20</td>
<td>1.0365</td>
<td>16.17,18</td>
<td>1.1916</td>
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<tr>
<td>7</td>
<td>24</td>
<td>0.9494</td>
<td>19,20,21</td>
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<tr>
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<td>0.9728</td>
<td></td>
<td></td>
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<tr>
<td>9</td>
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<tr>
<td>10</td>
<td>48</td>
<td>0.7099</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

* Notice that the axial zones are grouped by three due to the fact that the available data distinguish only seven axial zones. However, a 21 zones model is used in the MARCH calculations.

3.5.- MELTDOWN MODEL.

From the meltdown models available in the MARCH/CTN1 code version, it was decided to use for the basic case the meltdown model E, provided that it has a more mechanistic representation of the meltdown phenomena.

The parameters chosen for this model are represented in table 4.

Notice that the MARCH3 code uses a constant (temperature independent) value for the volumetric heat capacity, and the same is true for the new meltdown models implemented. Therefore these values has been chosen as representative in the temperature interval of interest.
Table 4.- Meltdown model E parameters.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
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<tbody>
<tr>
<td>TAG</td>
<td>1073 °K</td>
</tr>
<tr>
<td>TSS</td>
<td>1723 °K</td>
</tr>
<tr>
<td>TZR</td>
<td>2033 °K</td>
</tr>
<tr>
<td>TDEB</td>
<td>2248 °K</td>
</tr>
<tr>
<td>TMELT</td>
<td>2880 °K</td>
</tr>
<tr>
<td>TFUS</td>
<td>3550 °K</td>
</tr>
<tr>
<td>RCAG</td>
<td>2500 KJ/m³/°K</td>
</tr>
<tr>
<td>RCSS</td>
<td>4500 KJ/m³/°K</td>
</tr>
<tr>
<td>RCRZ</td>
<td>2800 KJ/m³/°K</td>
</tr>
<tr>
<td>RCUC</td>
<td>3900 KJ/m³/°K</td>
</tr>
<tr>
<td>COMPF</td>
<td>0.5</td>
</tr>
</tbody>
</table>

4.- BASIC CALCULATION RESULTS.

The results presented in this chapter have been obtained with the model described in the previous chapter, using the meltdown model E. They correspond to all the four phases of the Analysis Exercise, and the comments and discussions have been also divided in these four phases, in order to analyze the behaviour of the models according to the phenomena observed in the accident.

4.1.- PHASE 1.

The behaviour of the model in the first 100 minutes seems to be roughly correct. There is an initial fast despressurization (figure 2) during the steam blowdown through PORV, and after that the pressure rises again as the steam generators become dry. When they revet, the pressure falls again and it is stabilized at about 7 MPa, that is a value in close agreement with the measured pressure.
Figure 2. Primary system pressure (model B).

Figure 3. Primary system temperatures (model B).
Figure 4. - Maximum core temperature (model E).

Figure 5. - Steam generator secondary water inventory (model E).
Figure 6.- Steam generator heat transfer rate (model E).

The primary system temperature and saturation temperature (figure 3) both follow the pressure behaviour. The system is at saturation conditions during all this phase, and the core is well cooled as can be seen in figure 15, where the core maximum temperature is shown.

On the secondary side, the steam generator water inventory decreases very sharp and it becomes dry at about one and a half minute, which is a value in close agreement with the observed one. See figure 5. At about 8 minutes, the inventory starts to recover due to the restored supply of auxiliary feedwater. The steam generator heat transfer rate (figure 6) behaves accordingly.

4.2.- PHASE 2.

This phase starts at 100 minutes into the accident, with the core
covered and coincident with the primary circuit pumps stopping. However, the mixture level in the vessel is decreasing sharply (figure 7) as the high pressure injection is at a very low rate with the PORV still open. figure 1.

The secondary side pressure is also decreasing due to the depressurization started by the operators at 90 minutes, which is modelled as a linear ramp that reaches 100 psi at 200 minutes. As a consequence, the primary system pressure (figure 2) is also decreasing with this slope. The PORV flow leak rate (figure 1) is also decreasing as a consequence of the pressure reduction.

Figure 7. - Core swell water level (model E).

As can be seen in the previous figure, the sharp decrease in the water inventory in the vessel causes the level to reach the top of the core at about 102 minutes, that is a value in well agreement with the estimated uncover time of the accident. As a consequence of the core uncover, it
starts to heat up, as is reflected in figure 4.

At 139 minutes on the accident, the valve in series with PORV is manually closed, stopping the leak flow (figure 1). By this time, the pressure (figure 2) and the water swell level in the core (figure 7) both start to recover, due to the continued injection of the ECC. The minimum water level predicted to occur during this phase is of 1 meter, a value in agreement with the estimated minimum level of 80 centimeters.

Due to the core heatup during this uncovering phase, at about 139 minutes the conditions to sustain a metal-water reaction are reached and the cladding begins to oxidize (figure 8).

The oxidation process of the cladding produces hydrogen (figures 9 and 10) and heat, which in turn heats up the core further. This acceleration can be seen in the maximum core temperature (figure 4), that reaches the melting temperature for the ceramic oxides fraction, and therefore some melted fraction is predicted (figure 11).

![Graph](image)

**Figure 8.** Fraction of clad oxidized (model E).
Figure 9. - Hydrogen production rate (model E).

Figure 10. - Total hydrogen produced (model E).
Figure 11.- Fraction of core melted (model E).

As another consequence of the metal-water reaction, the hydrogen generated is accumulated in the upper parts of the primary circuit, which in the CANDU version of MARCH3 is modelled. This "hydrogen blocking" reduces the steam condensation onto the steam generator (figure 6) and therefore causes a pressure increase (figure 2).

The core materials are modelled to relocate during this phase, due to the high temperatures existing in the core. This is supposed to cause the pressure to rise further from 145 to 160 minutes, due to the "quenching" of the relatively hot melted materials that fall below the water level, figure 7. At 160 minutes, almost all the metals have fallen, and therefore the pressure rise is stopped and reversed. This is supposed to be caused also by the higher steam condensation onto the steam generator, due to the higher steam partial pressure and the stopping of the hydrogen generation (figure 9).

The metal-water reaction has been stopped, due to the fact that by this time all the metallic zircalloy has fallen into the water pool, and no metal-water reaction is assumed to occur below the water level.
A picture of the core state at the end of phase 2 can be observed in the following figures 12 to 16, where the materials distribution in the core is shown, for the four classes modelled. The figures represent half core, being the center at left. All the numbers are expressed as percentages of the initial volumetric fraction, taken as 100%.

The spaces that have no number inside, represent empty nodes. The numbers have been rounded for simplicity.

When analyzing this figures, it is important to keep in mind that the minimum water level reached during phase 2, is roughly 1 meter, that correspond approximately to the axial node 6 elevation in the graphs. This water level determines where the "cold nodes" are, and therefore where will tend to resolidify the melted material that is relocating inside the core.

The lower nodes, that have not been uncovered, represent the volumetric fractions of the intact nodes. This means that initially all the nodes were initialized to these fractions.
<table>
<thead>
<tr>
<th>R</th>
<th>1</th>
<th>2</th>
<th>3</th>
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Figure 12.- FMASS distribution at 175 minutes (model E).

(expressed as percentage of initial FMASS)
Figure 13.- AGFR - AGFRV distribution at 175 minutes (model E).

(expressed as percentage of initial FMASS)
Figure 14. - SSFR distribution at 175 minutes (model E).

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![Table](image)

Figure 15. - ZRFR distribution at 175 minutes (model E).

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Figure 16: UQPR distribution at 175 minutes (model P).

(expressed as percentage of initial FMASS)
In figure 12 the volumetric mass fractions can be seen for each node. They show no relocation at all in the first four levels, with certain relocation in the fifth and sixth levels. A complete solid crust is present, which corresponds to $F_{\text{MASS}} = 222$, covering the entire core area between levels six and eight. This crust is mainly formed by metallic compounds that have filled up the spaces between the fuel rods, according to figures 13, 14 and 15.

An upper debris bed is present in the upper part, shown by $F_{\text{MASS}} = 111$ which correspond to the chosen compactation degree of 50%. This debris bed extends for the first nine radial levels, while the nodes remain as non-debris nodes in the outermost radial region.

The uppermost four levels show void nodes, as a result of the compactation phenomena modelled.

Analyzing figures 13, 14 and 15, it can be observed that the control material is the one that reaches the lowest parts of the core, showing some relocation at level five. However, the other metallic classes relocate in the lower part of the core, between level six and eight, being the upper part of the core absolutely empty of metallic material.

In figure 16 the fraction of ceramic materials can be seen, and it is relocated from the level eight and upper, in the interstitials left by the previously relocated metallic material. The debris bed is also evident, and composed purely of ceramic oxides, while the outermost radial region presents its ceramic oxides intact.

Notice that this fraction, UOFR, takes into account both the uranium dioxide and the zirconium dioxide, and therefore the increments in this fraction in the upper nodes, are due to the oxidation process, and not to the relocation phenomena.
4.3. - PHASE 3.

Phase 3 starts with the pump B transient at 174 minutes, with the core severely damaged. This transient is simulated as a transfer of 30 m³ of water from the dead volume (VDED) to the vessel, in one minute.

This water addition immediately reflects on the water level (figure 7), that shows a sharp increase and covers completely the core. The boiling rate in the core increases also very fast, and it is reflected in the pressure (figure 2). As the steam partial pressure increases, the heat transfer in the condensing mode of the steam generator (figure 6) shows a peak, however limited due to the presence of hydrogen.

The calculation predicts that about 300 kgs of hydrogen were generated during phase 2, and they produce the steam generator blockage. However, it is supposed that more hydrogen was produced at TMI, and the lower prediction is the cause for the lower pressure peak at phase 3, compared with the TMI data base. However, the overall shape of the pressure curves during this phase are quite similar. As no metal-water reaction is modelled in MARCH3 for the nodes covered by liquid water, this reaction is virtually stopped at about 160 minutes, and no hydrogen is produced from that time on.

During the TMI accident, a relatively large molten pool was estimated to be formed in the center of the core. This fact is not predicted in this calculation, as a very small melted fraction appears, maybe due to the relatively weak metal-water reaction. This fact causes the core temperature to decrease significantly during the core reflooding (figure 4).

At the end of phase 3, two openings and closures of the block valve are also simulated (figure 1), which causes some hydrogen to leave the primary circuit, as it is shown in figure 17. This hydrogen leak causes the steam generator heat transfer phenomena to be more efficient, as it is shown in figure 6.

This valve openings are also the cause for the reduction in the pressure at the end of this phase, as it is shown in figure 2.
4.4. - PHASE 4.

This phase starts with full ECC injection at about 200 minutes. This cold water injection causes a significant decrease in the primary system temperature (figure 3) that drives the coolant to a subcooled state. This fact is also reflected in the pressure curve (figure 12), as the steam is allowed to condense in the ECC water.

From about 220 minutes on, the block valve in series with PORV is opened by the operator, which has been modelled (figure 1). This causes the pressure to be further reduced, and the water inventory is the consequence of the balance between the ECC injection and the PORV leak.

Regarding the core melting processes, as the core is completely refrozen at the beginning of this phase, the relocation of molten material that is supposed to have occurred at 224 minutes is not possible, and therefore the pressure curve does not present the increase that corresponds to the quenching
of these molten material in the lower head of the vessel.

The final state of the relocated material predicted for the code, at the end of phase 4, is roughly the same as was presented for the 175 minutes.

5.- MELTDOWN MODELS COMPARISON.

A comparison of the results obtained with different meltdown models, available in MARCH3/CTN1 has been performed. The main objectives of this comparison are, first, to visualize the different predictions on the behaviour of the plant, and therefore the sensitivity of the calculation to the meltdown model chosen, and second, to evaluate the CPU time required for each calculation.

The basic model described in chapter 3 has been used, with the exception of the meltdown model option. Two models, besides model E, were analyzed, model A and model D.

Model A is the basic meltdown model of MARCH3(STCP), and that of MARCH version V192 was used. This means that no melt consolidation is modelled. The model parameters have been chosen in such a way that no relocation to the bottom head is allowed to occur during the simulation of the Analysis Exercise. This means that no material relocation at all occurs when using this model.

Model D is described in reference /7/, and it model homogeneous core material relocation inside the core, when some nodes reach TFUS.

The meltdown temperatures used for this comparison TMLT and TFUS, have been chosen the same as for model E. The volumetric heat capacity RHOCC of the "homogeneous" core material for models A and D has been calculated as:

\[
AGFRV \times RCAG + SSFR \times RCSS + ZRFR \times RCZR + UOFR \times RCU0
\]

in order to make the results comparable to the previous model E calculation.
5.1.- MODELS PREDICTIONS.

The pressure behaviour predicted by the three meltdown models A, D and E is presented in the next figure 18. It can be seen that the predictions of the three models are identical during phase 1, which was expected, as the new meltdown models introduced do not affect the basic models of MARCI. The differences begin to appear in phase 2, when the meltdown models are used.

The pressure behaviour at the beginning of this phase 2 seems to be quite similar for the three models, with the exception that model E presents a pressure recovery from 140 to 162 minutes approximately, attributed to the fall into the water pool of melted metals and their subsequent quenching. This recovery of the pressure is not present in any of the other models predictions. However, when comparing the pressure behaviour to the THI data base values for this time period, the model E prediction seems to be more accurate.

Figure 18.- Primary system pressure (models A, D and E).
This is not the case from 162 to 174 minutes, when the model E calculates a pressure decrease that was not observed in the TMI accident. This is attributed to the stopping of the metal-water reaction for the nodes below the water level, where the melted zircalloy has relocated, as was already discussed in the previous chapter. As both models A and D do not model any zircalloy relocation, the metal-water reaction during this time continues to be active in the non-covered nodes, unless the available zircalloy is exhausted. This fact is reflected in the next figure 19 where the total hydrogen produced predicted by the three models is represented.

First of all, it is seen that the metal-water reaction starts at the same time for the three simulations, around 130 minutes. However, model E presents a higher oxidation rate than the other two models, this is attributed to be due to the double side oxidation model implemented in accordance with meltdown model E.

![Graph](image)

**Figure 19.** Total hydrogen produced (models A, D and E).
As it was already pointed out, the metal-water reaction for model E is virtually stopped at 160 minutes approximately, as the melted zircalloy falls below the water level in the core. This is not the case for models A and D, where the metal-water reaction continues until the end of phase 2. The total amount of hydrogen predicted for the three models are: 523 kg for model A, -21 kg for model D, and 304 kg for model E. All the predictions calculate the hydrogen to be produced during phase 2.

These different metal-water reaction total amount predictions, and their associated heat addition to the core, determine the amount of melted fraction, that is presented in the next figure 20 for the three models. Model A predicts the higher amount of core melted, almost one half, and the melted material is quenched during the pump transient at the beginning of phase 3. This is due to the fact that the model A used, without consolidation, does not change its core geometry due to melting.

![Figure 20. Fraction of core melted (models A, D and E).](image)
Model D presents a similar behaviour during this phase 2, with some small differences due to the relocation process of homogeneous core material, and a smaller maximum melted fraction, in accordance with the smaller metal-water reaction amount. This, in turn, is due to the relocation process that changes the exposed area of the core material.

Model E presents an almost insignificant amount of melted material. This is supposed to be due to the smaller amount of metal-water reaction predicted. This, in turn, is due to the metallic zircalloy relocation below the water level, and the lack of an oxidation model for nodes below this level.

A completely different behaviour is presented by model D for the long term. In this case, the formation of a melted pool is predicted, which corresponds to one third of the core approximately, and is not quenched by the water injected during the pump transient at the beginning of phase 3. A better picture of this behaviour can be observed in the next figure 21 where the FMASS distribution for 175 minutes is shown. It must be noticed that by this time the core is completely covered by the water injected during the pump transient.

Analyzing figure 21, the relocated homogeneous material can be seen between axial levels 8 and 14. The partially or totally melted nodes are indicated by an asterisc (*) in the figure, and the formation of both an upper and a lower crust can be observed. This is due to the model that allows the refrigeration of the nodes in contact with the water. It can also be observed that some nodes remain intact in the outermost radial region, representing material that "hangs" from the top. In the model, this means that these nodes have never been totally melted.

This molten pool and crust formation behaviour was also expected to occur when using model E, and it is supposed that the weak metal-water reaction predicted is the cause to their absence. The metal-water reaction is expected to continue in some nodes below the water level, when their temperatures are high enough, and this is not modelled.
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**Figure 21.** - FMASS distribution at 175 minutes (model D).

(expressed as percentage of initial FMASS)

(* indicates melted nodes)
Regarding the thermal-hydraulic behaviour of the models in the long term, it must be observed that the different hydrogen production predictions (figure 19) causes a much different pressure behaviour (figure 18), that in turn causes the leak throughout PORV to be much different, and therefore a comparison in not useful.

5.2. CPU CONSUMPTION.

All the calculations have been performed in a microVax II, and with the same timesteps chosen by user input. The comparative table of the CPU times required for the full four phases calculations presented follows:

<table>
<thead>
<tr>
<th>Meltdown model</th>
<th>CPU time consumed</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>2h 54m</td>
</tr>
<tr>
<td>D</td>
<td>1h 56m</td>
</tr>
<tr>
<td>E</td>
<td>1h 37m</td>
</tr>
</tbody>
</table>

As it can be seen, these results are the opposite to the expected, as long as the more sophisticated models consume less CPU than the simpler ones. The explanation to this has been attributed to the fact that the more sophisticated models predict a lesser amount of metal-water reaction, and the metal-water reaction calculation is the more time-consuming model in MARCH3. More than that, the new models provide some means of optimizing these calculations (for example bypassing it for full or empty nodes), and therefore enhancing the code speed.

In any case, it is clearly shown that the new models do not increase the CPU requirements of MARCH3, that are mainly dominated by the metal-water reaction calculation and timestep reduction scheme.
6. CONCLUSIONS.

Several calculations have been performed for the TMI-2 Analysis Exercise with the MARCH3/CTN1 code. Despite its very simple representation of some phenomena, quite good results have been obtained regarding the main phenomena that was observed in the complex TMI-2 accident.

With such a simple code, several runs of the whole transient (four phases) can be carried out in a reasonable amount of CPU time, and the phenomena relevant to severe accidents and source term calculations seem to be coherently modelled.

The results provided by the new models incorporated in MARCH3(STCP) are in good agreement with the expected ones, and the main limitations of the models have been pointed out.

The fast-running characteristics of MARCH3 have been maintained, as well as its general usage, and the new models allow to consider in-core relocation in a more mechanistic way.
7. REFERENCES.


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1 This work is sponsored by the CSN (Nuclear Safety Council of Spain) and UNESA (Unidad Eléctrica, SA).
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1. INTRODUCTION.

The participation of the Chair of Nuclear Technology from the Madrid Polytechnical University in the TMI-2 Analysis Exercise, under the sponsorship of the Nuclear Safety Council and Unidad Eléctrica S. A., from Spain, was taken as an opportunity to gain experience about severe accident phenomena modelling, to evaluate the capabilities of their available computer tools and also to upgrade them. One product of this participation is a new version of the MARCH code, which was used in Spain to perform the calculations presented herein.

It must be pointed out that all the modifications made to the code are of general application in nature; this means that the TMI-2 experience has been used to upgrade the MARCH code, modifying some physical models and adding some new ones, but avoiding the use of TMI-specific models, which may put limitations on the versatility of the code.

This working principle tries to take advantage from the TMI-2 evidence, as the only full-scale meltdown "experiment" up to date, keeping in mind that this is a peculiar plant in some aspects. This implies a compromise in the models and assumptions made, that has been carefully addressed.

More than that, the evidence of the processes occurring during another meltdown experiments, like the CORA series, and from other code models, has also been used to coherently model some of the important phenomena of a meltdown accident.

This appendix summarizes the modifications performed on the MARCH3 (STCP) code, version V192, at the Chair of Nuclear Technology, to produce the so-called MARCH3/CTN1 code version. Prior to that, a brief description of the MARCH(STCP) code capabilities is presented.
2. - CODE DESCRIPTION.

2.1. - MARCH3(STCP) CODE.

The MARCH3 code has been developed to analyze the thermal-hydraulic response of the reactor core, the primary coolant system and the containment system for light water reactors in response to accidents involving some level of engineered safety feature inoperability. While MARCH3 is primarily intended to address accidents leading to complete core meltdown, it can also be used to treat events involving only partial core degradation, as well as to assess the minimum levels of engineered safety feature operability required to cope with various accident events. As part of the NRC's Source Term Code Package (STCP), MARCH3 is designed to cover the entire accident sequence, from the initiating accident event through the attack of the containment basement, for a variety of accident initiators and including coverage of a wide variety of reactor system designs.

More specifically, the code can evaluate:

(1) Heatup of the primary and secondary coolant inventories and pressure rise to the relief/safety valve settings with subsequent boiloff.

(2) Generation and transport of heat within the core and associated coolant, if any, including boiloff of water from the reactor vessel.

(3) Heatup of the fuel following core uncover, including the effects of metal-water reactions.

(4) Melting and slumping of the fuel onto the lower core support structures and into the vessel bottom head.

(5) Interaction of the core debris with residual water in the reactor vessel.

(6) Interaction of the core debris with the reactor vessel bottom head.
(7) Mass and energy additions to the containment associated with all the foregoing phenomena and their effect on the containment temperature and pressure response, including the effects of passive and active containment safety features, as appropriate.

(8) The effects on the containment pressure and temperature response of the burning of hydrogen and carbon monoxide.

(10) Leakage to the environment through preexisting openings, due to venting, or late containment failure.

The above phenomena are treated in a self-consistent manner within the assumptions and approximations inherent in the code. The modeling approach is to treat well understood phenomena realistically on a level of sophistication consistent with the needs. For phenomena which are not well understood, there are a number of user-specified options in the code, that may be selected to explore the effect of various modeling assumptions.

An overall description of the MARCH3(STCP) code can be found in reference /1/, and a very detailed description of the MARCH models, in reference /2/.

2.2. - MARCH3-CTN1 CODE VERSION.

The original MARCH3 code from the STCP has been modified at the Chair of Nuclear Technology, with two different objectives:

- to make the code more flexible with respect to their input possibilities.

- to make some of the code models more "realistic", either by suppressing or modifying some unphysical models, or by adding some new ones not available before.
All the modifications have been performed keeping in mind the general use that is devoted at present to MARCH3. This means that they are also applicable to any of the LWR plants which MARCH3 addresses, and not specifically to the Babcock and Wilcox design plant like TMI-2. TMI-2 evidence is the basis where this changes have been made, and against which the new models have been checked.

In the following paragraphs, all the modifications are summarized and discussed.

2.2.1.- **Time-dependent input arrays.**

The time dependence of some input variables was introduced in the MARCH input. This is the case of the auxiliary feed water flow, the recirculation pumps operation, the high pressure injection and the letdown flow. These variables can be incorporated as tabular functions (time, value). This modifications do not affect the physical models of the code and therefore will not be discussed further.

2.2.2.- **Mass and energy balance.**

2.2.2.a.- **Makeup/letdown flow.**

It was not possible to represent adequately the high pressure injection and letdown flow of TMI, as the data available provide the high pressure injection rates (makeup and emergency core cooling) together, separately from the letdown flow rate.

Therefore, it became necessary to modify the balances to account for a makeup flow that is different from the letdown. This was done adding to the mass balance equation the injected flow. The term that considers the makeup/letdown flows was not included before, as both flows were supposed equal. For the energy balance equation, the term that computes the makeup/letdown energy was removed.
With these modifications, the user may specify by input the desired combination of makeup/ECC and letdown flows, in tabular arrays.

2.2.2.b.- Two volumes representation.

In order to compute correctly the mass balance when the water level in the vessel falls below the cold leg level, a new two-volumes model was implemented.

The original MARCH3(STCP) already considers two volumes, being one of them (VDED) a volume that contains the mass of water that can be stored in lower parts of the circuit during uncovery periods. This volume is fixed and is full of water, unless the vessel empties completely.

The two volumes concept, represented in figure 1, considers that the amount of water in VDED may not be constant. The volume can be full, partially full or even empty.

This dead volume (VDED) receives the condensed water that comes from the steam generator, and the letdown flow is taken from it, while in the main volume (VOLPX-VDED), the ECC and makeup flows are injected, and the valve and break leaks are taken off.

As both volumes are connected by the cold leg, water is spilled from the main volume to the dead volume if the water level is higher than YLEG, unless VDED is full. Coherently, if VDED receives more water than the amount it can store, it is automatically transferred to the main volume.

The user may specify by input, also, a water transfer from one volume to the other, by an input array table (flow, time). This option has been used to represent the pump transient that occurred at the end of Phase 2 in the TMI-2 Analysis Exercise.

With this model, a coherent representation of the mass balances occurring during a core uncovery period can be obtained, and some special situations (as
the pump transient can be simulated without falling into case insularity.

\[ \text{Figure 1. Two volumes representation.} \]

2.2.2.c. Steam condensation onto ECC water.

During the uncover periods, the steam present in the primary system may condense over the injected, relatively cold ECC water. A model is provided in the CTN1 version that condenses steam when the cold leg is uncovered. In this case, the condensation is limited to an efficiency (input option) that represents the fraction of the temperature increment that would drive the ECC water to saturation condition.
The condensed steam is added to the ECC injected mass, therefore maintaining the mass balance, and its enthalpy is coherently recalculated.

2.2.3. - **Steam generator.**

Several modifications have been performed in the steam generator models, that are described in the following subsections:

2.2.3.a. - Steam condensation mode.

There was a limitation to the steam condensation on the primary side, when the core boiling rate was low or zero. This limitation was unphysical and therefore it was removed.

The steam generator is allowed to act in the condensing mode when the water level in the vessel is lower than a leg height, YLEG2, which is an input variable. This YLEG2 may have the same value as YLEG, depending on the primary circuit layout, but it has been found that some numerical oscillations in the heat transfer rate may appear when the water level is just at YLEG. This situation is common to occur due to the two volumes representation. Therefore, it is recommended to use a slightly different value for the cold leg (YLEG) and for the condensation leg (YLEG2).

2.2.3.b. - Stabilizing factors.

The temperature difference and pressure difference between primary and secondary side were used in the SGEN routine to correct the heat transfer rate as stabilizing factors, that applied at all the operating ranges. This fact was modified to take into account this stabilizing factors only when the differences are small. This is supposed to model the heat transfer rates more realistically when the thermalhydraulic conditions for both sides of the steam generator are basically different.
2.2.3.c. - Dryout criteria.

The dryout criteria was modified to occur when the water inventory in the secondary side reaches 5% of the total capacity of the secondary side. Coherently, the water level is allowed to increase until full capacity, which may be different from the initial steam generator level.

2.2.3.d. - Hydrogen blocking.

The MARCH3 code takes into account the presence of hydrogen in a very approximate manner, that of using for the heat transfer calculation the steam partial pressure, rather than the total primary pressure. This approximation supposes that the hydrogen is homogeneously distributed along the circuit and does not model the "hydrogen blocking" phenomena observed at the end of phase 2 of the TMI accident.

In order to simulate this hydrogen blocking, a new model was introduced in the SGEND routine. This model acts as a limitation to the heat transfer rate when the steam generator is operating in condensation mode and hydrogen is present in the primary circuit.

The calculated heat transfer rate in these conditions is multiplied by a factor $UC$ of the type:

$$UC = ((PSV/PHV)^{0.8})/FAC$$

Where:
- PSV is the steam partial pressure
- PHV is the hydrogen partial pressure
- FAC is a scaling factor (plant dependent)

This factor is limited to one on its upper value, and has been derived from the Uchida correlation for steam condensing in the presence of non-condensible gases.
The model can be activated or de-activated by user option at different times, and a FAC of 5 has been found satisfactory for the TMI plant.

2.2.4. Meltdown model D.

MARCH3(STCP) provides two meltdown models for an homogeneous core. The models are described in reference /2/ and are known as the A model (that favours an up-down heating process) and B model (which favors a down-up heating process). None of these models consider in-core relocation, and they are complemented with a C model that simulates the phenomena of nodes falling to the lower head of the vessel, when certain input conditions are satisfied.

The TMI-2 accident, and several experiments, have confirmed the existence of in-core relocation of core materials, particularly when the core is not completely uncovered, and therefore a water level, relatively cold, is present. With this fact in mind, a new meltdown model for an homogeneous core material, known as D, was implemented in the CTN1 version. This model is activated by input option and it is described in the following paragraphs.

2.2.4.a. Temperature revaluation.

The meltdown model D makes two steps: in a first step, a temperature revaluation is performed, that is identical to that of model A, this means that the up-down heating process is favored.

The model takes the node temperatures calculated in the BOIL routine, after considering the heat extractions and additions for each node. Then, a "melted region" for each radial zone is defined, starting from the uppermost node whose temperature exceeds the fusion temperature TFUS (input option), and transferring the excess heat to the node immediately downside, until the average temperature of the melted region is lower than TFUS. In the case the lowermost node is reached, the whole melted column of nodes will fall down, following the C simulation model.
When a "stop node" is found, the temperatures of the melted nodes are redefined at TFUS, the temperature of the stop node is redefined to a value such as to maintain the overall energy balance, and the nodes at TFJS are going to relocalize in the next step of the model D.

A check is provided for the case when more than one melted region exist in a given radial region.

2.2.4.b. - In-core relocation.

Once the temperatures are redefined, those nodes with a temperature equal to TFUS will relocalize inside its own radial region. These nodes are assumed totally melted (liquid) and therefore they will consolidate without porosity.

In model D, a volumetric fraction occupied by the node material is defined, and initialized to unity in the intact nodes. A maximum volumetric fraction (FMMAX) is defined, as the maximum volume that can be stored, referred to the initial occupied volume.

With the maximum fraction, a node is considered full. Therefore, the melted nodes relocalize from the "stop node", until its completion is reached, then, they relocalize in the immediately upper node, and so on.

It must be noticed that more than one relocation zone may exist for a given radial region, and that the relocation process occurs only in the axial direction. This means that no relocation is modelled to occur between different radial zones.

The definition of FMMAX is, for example, in a regular square-like rod configuration:
Figure 2. - FMAX definition.

\[
\text{FMASS (intact)} = \frac{1}{\pi r^2} / \pi r^2 \\
\text{FMAX} = \text{FMASS (full)} = \frac{p^2}{\pi r^2}
\]

When some node material relocates, a fission product relocation is also computed, for the 40 CORSOR groups. The relocation model considers the node fraction that is relocating (and consequently the fission products fraction) to take into account the case when the "receiving node" is partially full.

The relocation process is computed node by node, in order to prevent the "mixing" of "upper" fission products with "lower" ones, bypassing some existent nodes. Means are provided to optimize the process, for example bypassing the calculation for already full nodes.

Finally, a peaking factors "relocation" is also performed, to take into account the fact that nodes with a certain proportion of decay heat may fall into zones of different decay heat fraction. The model maintains the decay heat power balance, therefore changing the axial power profile for each
radial region from its original shape, when the nodes relocalize.

In the case that the lowest melted node is the first of a given radial region, all the melted nodes are assumed to relocalize to the bottom head. The model used for this relocation is the same model C of the MARCH? (STCP) code.

With these concepts, the model D relocation simulation maintains the overall balances of mass and energy, considers the maximum mass possible to fit into a given node, and also redefine the CORSOR groups inventories, in order to coherently model the fission product loss calculations.

2.2.4.c.- Heat transfer models modifications.

The partial relocation of melted nodes obviously changes the area of these nodes available for heat transfer phenomena, and this fact is taken into account in model D.

According to this model, each individual node may have a different volume fraction, ranging from zero to FMMAX, it means, empty, partially full, or full. Based on the volume fraction of each node, a criteria was established to modify its exposed area, as follows.

When the node is increasing its mass above the initial one, the exposed area for the heat transmission mechanisms is maintained constant at its initial value, until a critical volume fraction (FCRIT) is reached. From that time on, a factor FTN is defined:

\[ \text{FTN} = \frac{\text{FMMAX-FMASS}}{\text{FMMAX-FCRIT}} \]

When the node is decreasing its mass below its initial value, FTN is defined as follows:

\[ \text{FTN} = \frac{\text{FMASS}}{\text{FMASS(initial)}} \]
Then, the available "exposed" area is multiplied by the factor \( F \), to obtain the actual area that will be used for the heat transfer processes.

Note that when the node is completely full or empty, its exposed area is zero. Computational means are provided to prevent the divisions by zero in these cases. Note also that this model does not allow the exposed area to exceed its initial value.

A criterion was needed to establish the value of \( F_{CRIT} \), and the following hypothesis was used: it was supposed that the melted material flows down surrounding the existing solid rods of the lower nodes, in a very simple candling model, see figure 3.

![Diagram of reorganized material](image)

**Figure 3.** \( F_{CRIT} \) definition.

The exposed area is supposed to be unchanged until the material of one rod reaches the material of the adjacent rod, this means:

\[
F_{CRIT} = \frac{(\phi/2)^2}{\pi r^2}
\]
This criteria is an approximate manner to take into account that the flowing material will increase the exposed area, but at the same time it will decrease the heat transfer due to the increased equivalent thickness.

It must be noted that the steam (or hydrogen) flows are not corrected due to the flow area decrease (or increase), and that these flows are assumed to occur in the axial direction inside each radial region, therefore none cross-flows between different radial regions are modelled.

For the nodes completely full, it has been said that their heat transfer area is set to zero. This has an exception, that is the case when the full node is axially adjacent to a non-full node. In this case, the exposed area to the fluid is set proportional to the difference of the two adjacent contact volumetric fractions (that are equivalent to areas). This means that this exposed area can be expressed as:

\[ \text{ABASE} = \text{FMAX} - \text{FMASS(1)} \]

This area is used to calculate the heat transferred from the full node to the fluid (gas or liquid), coherently scaled to area of the given node.

For the axial conduction model, the heat transfer area for two adjacent nodes is set proportional to the smallest volumetric fraction of both nodes, in order to take into account their contact area.

With these exposed area reevaluations, model D provides means to coherently model the heat transfer processes between nodes, between nodes and the fluid, and also between nodes and structures by radiation, in accordance with the assumed relocation models. These are very simple models that represent not very well understood phenomena, as a step ahead to obtain a more realistic core degradation modelling.

The model D, installed in the CTN1 version of the MARCH3 code, has been checked and debugged against the TMI-2 data. The CPU times required, when compared to the A meltdown model, are quite similar, and it was found that
the metal/water reaction subroutine (ZRWATR) is responsible for the significant changes in the CPU time, independently of the meltdown model used. This means that significant increases of CPU are produced when a larger metal/water reaction is predicted, and the CPU increase due to the different meltdown model is insignificant (in fact, impossible to be measured) when compared to the previous one.

2.2.5. *Meltdown Model E.*

Due to the insensitiveness of the CPU time required for a MARCH3-CTN1 code version run on the meltdown model chosen, it was decided to develop a new, more mechanistic meltdown model E, that should be able to simulate the different behaviour of the relevant materials that constitute an heterogeneous reactor core.

From the TMI-2 samples examination program, it has been observed that different core materials were found in different locations of the degraded core. This fact clearly indicates that different materials behave different during the meltdown phase. There are many materials present in the core, and some of them were selected as "representative" for the model E. The intention was to physically represent the behaviour of the major constituents of the core during the meltdown phase.

Many phenomena occur during meltdown (both physical and chemical) and there are, at present, some computer codes that model some of these phenomena, more or less mechanistically. As MARCH3 is a code designed to be simple (and therefore fast-running), a compromise exists to develop a model able to represent the most important phenomena, in a simple manner, but as mechanistically as possible. This is the idea of model E.

With this purpose, four different materials were identified as relevant: ceramic oxides (zirconia and uranium dioxide), control material (Ag-In-Cd alloy), metallic zircalloy and metallic stainless steel. The reason for these four "classes" is due to its relevance in the total amount of the core materials.
Model E is structured in a series of submodels, that act sequentially at each time step. These submodels are, in order: temperature revaluation, control material relocation, stainless steel relocation, metallic zirconium relocation, melted ceramic oxides relocation, and in-core debris formation and relocation.

Model E is coherently coupled with the decay heat generation model, heat transfer models, metal/water reaction model, fission products loss model and drop to bottom head model. It is described in the following paragraphs.

2.2.5.a.- Node definition.

The state of a node is characterized by several variables:

FMASS indicates the volumetric fraction of the node materials, referred to its own initial FMASS. Initially equal to unity.

FMAX and FCRIT are defined as in model D.

AGFRV indicates the volumetric fraction of control material inside the cladding, referred to initial FMASS.

AGFR indicates the volumetric fraction of control material outside the cladding, referred to initial FMASS. Initially set to zero.

SSFR indicates the volumetric fraction of structural stainless steel, referred to initial FMASS.

UOFR indicates the volumetric fraction of ceramic oxides (zirconia - uranium dioxide) referred to initial FMASS.

ZRFR indicates the volumetric fraction of metallic zirconium, referred to initial FMASS.
Initially, the sum of AGFRV + SSFR + WQFR + ZRFR must be equal to FMAST, that is, unity. AGFR must be zero. This initialization is performed according to the geometric input provided by the user, and checked for consistency.

A particular case is that of ZRFR, that is calculated from the input variables, coherently with the clad thickness (CLAD) and the initial oxide layer thickness (XOO). These values are also input variables in MARCH3(3TCP).

For the energy balances calculations, a volumetric heat capacity is used for each node, that is calculated the volumetric heat capacities of the four material classes (RCAG, RCSS, RCZR and RCUO), all four input values. For each node, the total heat capacity (RHOCU) is evaluated at each timestep according with these individual capacities and its volumetric fractions.

2.2.5.b. – Temperatures revaluation.

The temperature revaluation for the totally melted nodes is performed in model E, in the same manner as it is done in model A. This means that an upside-down heating process is favored.

2.2.5.c. – Control material relocation.

The relocation process is performed in the axial direction for each radial region. The first step in the relocation process is to identify the lowermost node in a given radial region, whose stainless steel control rod cladding is supposed to be failed. This is done by comparing the node temperature with a user input melting temperature for the stainless steel, TSS. See figure 4.

Once this failed node is found, all the upper nodes in that radial region that are potentially capable to drain its control material content from inside the cladding (AGFRV) will do it, in case its temperature is above a control material melting temperature, TAG, user input. More than that, all the nodes above the failed one, whose temperature exceeds TAG, are assumed to
drain all its control material from inside the clad (AGFRV) and outside it (AGFR).

Figure 4. - Control material relocation scheme.

During the collection process, the mass and energy of the control material that will relocalize are stored as interim variables, as well as the inventories of CORSOR groups 21, 22 and 23. AGFR and AGFRV for the "donor"
nodes are coherently set to zero.

Once this collection is complete, the material is assumed to relocate instantaneously, starting from the first node (coming from upside) below the failed one, whose temperature is at least 300°K below TAG.

The relocation process can fill up some nodes, and in this case the excess mass is relocated in the immediately upper node. This means that a check is made to assure that the total volumetric fractions of the "receiving" nodes does not exceed VMHAX.

Special means are also provided to assure that the melted control material will not bypass a previously formed full and cold node (that can be considered as a crust node).

No means are provided to distinguish the nodes covered by the water level in the relocation process, but the use of the model has demonstrated that when a water level exists in the core, the nodes below it are quite cold, and the control material tends to relocate just below this water level.

After the control material relocation, the temperatures, volumetric fractions and CORSOR groups 21, 22 and 23 of the "receiving" nodes are reevaluated, in order to maintain the mass, energy and species inventory balances. For the energy balance, a user input control material volumetric heat capacity, RCAG, is used.

In the case that the melted control material reaches the bottom of the core, it means that the lowermost node has a temperature above (TAG - 300°K), the control material will relocalize to the bottom head. The mass and energy of this material is stored in the interim variables AGBOT and QOBOT, which will be considered at the end of the relocation process at current timestep.

2.2.5.d. - Stainless steel relocation.

After the control material has been relocated, the model proceeds to
the relocation of the stainless steel, represented in each node by SSFR.

The material that will relocate is identified by comparing the nodal temperature with the stainless steel melting temperature, TSS, which is an input parameter. Those nodes whose temperature exceeds TSS will relocate their SSFR. See figure 5.

![Stainless steel relocation scheme diagram]

Figure 5. Stainless steel relocation scheme.

The mass and energy of the stainless steel that will relocate are stored as interim variables, as well as the inventories of CORSOR groups 14, 17 and 18.

Once the collection of melted stainless steel is complete for a given
melted zone in a given radial region, the material is assumed to relocate instantaneously, starting from the first node (coming from upside of the melted region) whose temperature is at least 300°K below TSS.

As the relocation process can fill up some nodes, the excess mass is relocated in the immediately upper node to the full one, to consider that the total volumetric fraction of a given node cannot exceed PMMAX.

A check is made to relocate melted stainless steel if more than one melted zones exist in a given radial region, and they are not contiguous.

Special means are provided to assure that the melted material will not bypass a previous formed full node (that can be considered as a crust).

After the stainless steel is relocated, the temperatures, volumetric fractions and CORSOR groups 14, 17 and 18 inventories of the "receiving" nodes are reevaluated, in order to preserve the mass, energy, and species inventories balance. For the energy balance, a user input stainless steel volumetric heat capacity, RCSS, is used.

In the case that the melted stainless steel reaches the bottom of the core, it means, that the lowermost node in that radial region has a temperature above (TSS - 300°K), the melted stainless steel is assumed to relocate to the bottom head. The mass of this material is added to SSBOT, and its energy to the interim variable QOBOT, which will be considered at the end of the relocation process.

2.2.5.e. - Metallic zircalloy relocation.

A completely different treatment is devoted to the metallic zircalloy and to the zirconium oxide. The first one is represented by ZRFR in each node, and the second one is included in UOFR, together with the uranium dioxide. Transfer of material between ZRFR and UOFR, can occur when the metal/water reaction (zirconium oxidation) takes place, and this model will be discussed later in section k.
The metallic zircalloy is characterized by a melting temperature TZR, and a volumetric heat capacity RCZR, both input by the user.

![Diagram](image)

Figure 6.- Metallic zircalloy relocation scheme.

The metallic zircalloy relocation model is identical to the stainless steel model, with the exception that the related CORSOR groups in this case are 9, 12 and 19. The used melting temperature and heat capacity are, of course, TZR and RCZR. See figure 6. For drop to the bottom head, the related variables are ZRBOT and QQBOT.
2.5.5.1. Ceramic oxides relocation.

The ceramic oxides volumetric fraction of a given node is VOFR, which considers both the uranium dioxide and the zirconium dioxide. This fraction is characterized by a melting temperature, TMELT, a volumetric heat capacity, RCUO, and TFUS, all of them user inputs. TFUS is a temperature that considers TMELT plus the latent heat of fusion of uranium dioxide, in order to obtain the melted fraction of a given node, when its temperature exceeds TMELT, as:

\[ \text{melted fraction} = \frac{(T - TMELT)}{(TFUS - TMELT)} \]

and limited to unity. This TFUS concept was already implemented in MARCH3(STCP), and therefore will not be discussed herein.

The ceramic oxides relocation model is different from the previous ones presented for metallic compounds. First of all, the "donor" node may not be completely melted, and therefore only its melted fraction is capable to relocate. This is, that partial relocation of its volumetric fraction may take place.

Secondly, the ceramic oxides relocation is a "node by node" process, in order to prevent mixing of materials from different core regions, that may have a different burnup, and so different fission products inventories and power peaking factors. This is not the case for the metallic compounds already considered, as they are assumed to be homogeneously distributed in the core, and their concentration is not affected by the burnup.

The melted fraction for each node is obtained as it has been explained, using TMELT and TFUS, that is, taking into account the latent heat of fusion.

The relocation process for ceramic oxides starts from the bottom of the core, looking for a node I that has a temperature greater than TMELT. Once this node is found, a look for a deposition node, IDEP, is started downside. The deposition node must be at a temperature lower than \((TMELT - 300^\circ K)\), and of course it must not be full.
In the case that the deposition node is the lowermost one in a given radial region, the melted material is assumed to drop to the bottom head and the mass and energy that drops is stored in the interim variables UOFR and QQBOT, to be used at the end of the relocation process.

When the IDEP node is found, the melted material relocates inside it, until it is full, the excedent fraction relocates in the immediately upper node, and so on. See figure 7.

<table>
<thead>
<tr>
<th>Relocation Node IDEP</th>
<th>T &gt; TMELT</th>
</tr>
</thead>
<tbody>
<tr>
<td>Empty nodes</td>
<td></td>
</tr>
<tr>
<td>Partially full node</td>
<td>T &gt; (TMELT - 300°K)</td>
</tr>
<tr>
<td>FMASS &lt; FMMAX</td>
<td></td>
</tr>
<tr>
<td>Lowermost Deposition Node IDEP</td>
<td>T &lt; (TMELT - 300°K)</td>
</tr>
</tbody>
</table>

**Figure 7.** Melted ceramic oxides relocation scheme.

When a relocation takes place, the volumetric fractions FMASS, AGFR, ZFR, SSFR and UOFR are reevaluated, as well as the temperature, the fission
products fraction FP, the 40 CORSOR groups inventories, and the axial peaking
factors of both the "donor" and the "receiving" nodes. This revaluation
preserves the mass, energy, and species inventories balance, as well as the
decay heat generation amount. To perform the temperatures revaluation, the
volumetric heat capacities for all the four material "classes" are used
(RCAG, RCSS, R CZR and RCUO), all of them are input variables.

Once the relocation of node I has been modelled, the upper node (I+1)
is analyzed for relocation, and the process is repeated, until the uppermost
node, ND2, is reached.

2.2.5.g- In-core debris formation and relocation.

Meltdown model E incorporates a debris model, that considers certain
conditions for the formation of a "debris node" and for its relocation. The
phenomena involved in the debris bed formation during a severe accident is
not well understood, in particular it is not well known which conditions
should be satisfied to transform a stack of fuel pellets covered by a
zirconia layer into debris, and which are the debris characteristics, like
particle size distribution and degree of compaction (porosity).

In several experiments it has been observed that the fuel stacks are
quite resistant, even when they have lost all its metallic zircalloy. In the
TMI-2 accident the upper debris bed is supposed to have formed from fuel
stacks strongly perturbed during the pump transient at the beginning of phase
3 of the Analysis Exercise. However, this phenomena is not easy to introduce
into any predictive model.

In meltdown model E, the conditions for debris bed formation are as
follows: when a given node has experienced the melting of its metallic
zircalloy, it is supposed that the fuel pellet stacks are preserved in this
shape until a user input temperature, TDEB, is reached in the node. A
reasonable value for this temperature seems to be the melting temperature of
the \( \alpha - \text{Zr} \) (0), about 1975°C. In this case, the zirconia layer that covers
the fuel stack is supposed to loose its integrity, and the node is
transformed into a debris node.

To take into account the porosity of the debris bed, another user input parameter is defined, COMPF, that is a compaction factor.

This COMPF is used to determine the maximum amount of debris material necessary to fill up a "debris relocation node". This maximum amount is obtained by the product FMMAX*COMPF.

```
Non-debris node T<TDEB
Debris node       T>TDEB
Empty node
Lowermost deposition node IDEP
Full (for debris) node FMASS<FMMAX*COMPF
                          FMASS>FMMAX*COMPF
```

Figure 8.- Debris relocation scheme.

The relocation process for the "debris nodes" is based also on a "node by node" scheme, in order to prevent mixing of materials from different core
regions, that may have different fission product inventories and therefore different power peaking factors, to be considered in the decay power generation.

The relocation process starts from the bottom of the given radial region, looking for a node that has a temperature above TDEB, or has been debris in some previous timestep, or has space for storing debris (it means to be empty or with a volumetric fraction lower than PMAX*COMPF). Once this node IDEP is found, the immediately upper nodes are analyzed to look for possible upper debris nodes, that will relocate in IDEP until its completion. If IDEP fills up, the excedent debris material relocates in the immediately upper node to IDEP, and so on. See figure 8.

When a relocation takes place, the volumetric fractions FMASS, AGFR, SSFR, ZRFR and UOFR are revaluated, as well as the temperature, the fission products fraction FP, the 40 CORSOR groups inventories, and the axial peaking factors of both the "donor" and the "receiving" nodes. This revaluation is performed in such a way that the mass, energy, and species inventories balance is preserved, as well as the decay heat generation amount.

2.2.5.h. - Fission and structural groups relocation.

The relocation of the CORSOR groups is taken into account in both the meltdown models D and E. This is necessary to correctly compute the inventories considered in each node by the fission products release models, already implemented in MARCH3(STCP) and not changed in the CTN1 version.

When a relocation takes place in model D, or by ceramic oxide melting relocation or debris relocation in model E, the whole 40 CORSOR groups inventories are actualized, transferring to the "receiving" node the fraction of the inventory of the "donor" node that corresponds with the relocation fraction.

In meltdown model E, when relocation of melted control material takes place, the CORSOR groups 21, 22 and 23 are relocated, maintaining its overall
balance. These groups correspond to control rod materials silver, indium and cadmium.

When relocation of stainless steel takes place, the CORSOR groups 14, 17 and 18 are also relocated. These groups correspond to iron, nickel and chromium. For the relocation of metallic zircalloy, the CORSOR groups 9, 12 and 19, which correspond to zirconium, tin and manganese, are relocated.

With these models, the relocation process for the CORSOR groups is coherent with the overall relocation models. Therefore, the species balance and distribution is modelled in a consistent manner, to be used by the fission products release models properly.

2.2.5.1.- Decay power relocation.

During a severe accident the core relocation phenomena move the fission products, responsible for the decay heat, from one place to another, therefore moving the decay heat sources and modifying the initial power shape. This fact is also considered in the CTN1 version of MARCH3, both for meltdown models D and E.

In MARCH3, the decay heat fraction produced in a certain node, is obtained as the product of the fraction of fission products that remains in the node (FP) times the local peaking factor of the initial power profile (input by user). The fraction PP is actualized by the fission product loss models, that reduce this fraction according with the release models.

In the CTN1 models D and E, when material that contains fission products relocalize (homogeneous core material in model D, or melted ceramic oxides or debris material in model E), the fraction FP is also coherently relocalized.

The peaking factor of the "donor" node remains unchanged, while the peaking factors of the "receiving" nodes is coherently recalculated in order to preserve the decay heat generation balance.
This process produces deformations of the initial power profile, which are supposed to be more realistic than maintaining the initial power profile. In any case, the model is coherent with the mass and fission products relocation models.

2.2.5.j.- Heat transfer models modifications.

The heat transfer models modifications in model E, for those nodes that have changed its initial volumetric fraction FMASS in the relocation process, are performed in the same manner as it was described for model D.

2.2.5.k.- Metal/water reaction revaluation.

The metal/water reaction rate is calculated in MARCH3 code in the subroutine ZRWATR, for a rod-like geometric configuration, considering an initial CLAD thickness for the cladding, and an oxide thickness XOO that is reevaluated after the amount of reaction has been assessed for the given node. The model assumes one side oxidation of the cladding, with several reaction models for different conditions. See reference /2/.

Some modifications became useful to be performed in the metal/water reaction model when using the meltdown model E. However, it was decided not to modify the ZRWATR routine.

First of all, a double side oxidation model was implemented, doubling the available exposed area for those nodes whose cladding is assumed to be broken. The model is activated when the node temperature exceeds TWW2, a user input. A reasonable value for this temperature seems to be around 2000°F, which is the value implemented in MARCH3 to trigger the initial gap release in the fission product loss models. Means are provided to assure that the oxide thickness, when the clad is double side exposed, does not exceed half the cladding thickness.
As the ZRFR indicates the amount of metallic zircalloy that remains in a given node, this volumetric fraction is corrected after the oxidation rate for each timestep and node is evaluated. The oxide thickness formed during that timestep, is converted to volumetric fraction, and then it is subtracted from ZRFR and added to UOFR (ceramic oxides fraction). In this way the formation of zirconia is accounted for, transforming metallic zircalloy to oxide, in a node by node scheme.

A special model has been implemented for the case of a node that receives metallic zircalloy from a "donor" node that melts its own ZRFR. In this case, the "receiving" node is assumed to expose its ZRFR directly to the environment, and therefore its oxide layer thickness X00 is set to zero when this relocation occurs. More than that, when the metallic zircalloy comes to the receiving node, it will present a different reaction area to the steam, which is modelled to be proportional to the ZRFR of that node, in order to take into account the fact that nodes with more relocated metallic zircalloy will exposed a major area, compared with its initial one.

In meltdown model E, CLAD and X00 for each node are reevaluated prior to the call to subroutine ZRWAR, from the volumetric metallic zircalloy fraction ZRFR, and the past history of that node. An area multiplication factor is also evaluated, to take into account, after the call to ZRWAR, both phenomena of double side oxidation and zircalloy accumulation due to relocation process. However, the oxidation models of ZRWAR remain unchanged.

This means that the same oxidation models of MARCH3(STCP) are used, but the exposed area to oxidation is corrected in accordance with the failure of the cladding and with the relocation of melted metallic zircalloy.

2.2.5.1.- Materials drop to the bottom head.

A model is also provided to take into account the drop of melted material of any class (control alloy, stainless steel, metallic zircalloy or ceramic oxides) to take into account their mass and energy additions to the water in the bottom head, and its decay power in case ceramic oxides are
involved.

Four variables represent the inventories of the four material classes in the bottom head, AGBOT, SSBOT, ZRBOT and UOBOT. The fission product decay heat is considered to be added each timestep. The stored energy is considered for each material type, according to its temperature and its specific volumetric heat capacity (input values).

3. CONCLUSIONS.

Two new and more mechanistic meltdown models for the MARCH3 code have been presented, together with several thermal-hydraulic improvements, as the two-volumes model for the primary circuit. All these models seem to be correct, and correctly linked with the other MARCH3 models. The general applicability of the MARCH code has been maintained.

The results provided by these models for TMI-2 are in good agreement with the expected ones, and the main limitations of the models have been explored.

When comparing the CPU time requirements, it has shown that the new models do not increase the CPU requirements of MARCH3, that are mainly dominated by the metal-water reaction calculation and timestep reduction scheme. Therefore, the fast-running characteristics of MARCH3 have been maintained, as well as its general usage.

4. REFERENCES.


TMI-2 ANALYSIS EXERCISE: MARCH3 VERSION 192 RESULTS

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The Netherlands Energy Research Foundation, ECN, has participated in the TMI-2 Analysis Exercise to contribute to the understanding of severe reactor accident phenomena. The MARCH3 version 192 code, as it is available in the NRC's Source Term Code Package, has been applied to calculate the TMI-2 accident behavior. Much experience has been gained as far as the use and the limitations of this code. It is concluded that the code in its original form has too many shortcomings to analyse a complex accident scenario such as the TMI-2 scenario. Nevertheless MARCH3 has proven to be a practical tool for parameter studies. This Appendix elucidates these findings.

1. INTRODUCTION.

The participation of the Netherlands Energy Research Foundation in the Three Mile Island - Unit 2 Analysis Exercise has had the objective to get a better understanding with respect to:

- The phenomena which occurred during the TMI-2 accident.

- The capabilities of computer codes to simulate the behavior of a Light Water Reactor under severe accident conditions.

The first objective was satisfactorily fulfilled by the participation in the Task Group on Three Mile Island - Unit 2 of the OECD/CSNI Principal Working Group No. 2 on Coolant System Behavior. Direct access to data, as obtained from the TMI-2 Accident Evaluation Program, has led to a better understanding of severe reactor accidents and many data of TMI-2, especially on fission product behavior, have been used to constitute a source term reference in the Netherlands [1].

ECN has selected MARCH3, being the driver code of the Source Term Code Package (STCP), as developed at Battelle, Columbus, as a calculation tool for analysis of the TMI-2 accident. The main advantages of this selection has been:

- Good documentation for so-called "end users".

- Widely spread in various countries.

- Important NUREG reports are based upon calculations executed with the STCP.
In order to be able to judge the results of calculations published in literature, the MARCH3 version 192 of the STCP was used [2] as it has been released by the NRC. One code deficiency has been corrected and some minor updates have been made. No changes were made in the basic modelling of the code. Within the scope of the Netherlands participation in this final analysis report, no attention will be given to the models in the code: details of model assumptions are documented in [2].

The same applies to the input of the code: all input data was derived from the Plant Configuration report [4], the TMI-2 (electronic) Data Base for the sequence of events [5], and the initial conditions and boundary conditions [6].

In the next paragraphs, the code modification, the calculation method and the results of the analysis are presented. For an understanding of these results, it is assumed that the reader is familiar with the TMI-2 plant configuration and the accident scenario. In the last paragraphs, the significance of the results and the conclusions from the analysis exercise are given.

2. CODE MODIFICATIONS.

As mentioned in the introduction, only one significant code change has been incorporated in the MARCH3 version 192 code. This modification concerns the steam generator heat transfer subroutine SGEN. Calculations using the original version of the code produced a non-physical behavior of the power being exchanged in the steam generator. This can be noted from the stepwise power reduction in Figure 1. This discontinuous behavior appeared to be caused by a discontinuous behavior of the heat transfer expressions being applied in the code. Figure 1 also presents the TMI-2 heat exchange based on measured hot and cold leg temperatures in loop A. ECN has modified the subroutine SGEN in such a way that heat transferred inside the steam generator, $Q_{s generators}$, is only given by correlation E3.114 from reference [3]:

$$Q_{s generators} = H_{eff} \times A_{s generators} \times (T_{p2s} - T_{s}) \times \frac{W_{s}}{W_{s generators}}$$

where

- $H_{eff}$ = effective heat transfer coefficient
- $A_{s generators}$ = steam generator heat transfer area
- $T_{p2s}$ = primary side water temperature
- $T_{s}$ = secondary saturation temperature
- $W_{s}$ = mass in steam generator secondary

Figure 1: Comparison of heat transfer in steam generator.
$m_{i0}$: initial mass in steam generator secondary.

As shown in Figure 1, this code modification gives a better representation of the TMI-2 heat transfer.

In addition, some minor changes have been made in order to be able to use time dependent input variables. These updates however, do not affect the code physical models.

3. CALCULATION METHOD.

The most important boundary condition for the TMI-2 analysis is the heat removal by the steam generators. This heat removal controls to a large extent the thermohydraulic behavior of the primary system. Compared to this, the energy release via the stuck open Pilot-Operated Relief Valve (PORV) forms just a minor contribution. When the heat exchange by the steam generators is not adequately modelled, no correct thermohydraulic conditions inside the primary system can be calculated. For example, steam condensation in the cold leg due to High Pressure Injection (HPI) and degraded heat transfer in the steam generator due to hydrogen blocking are not modeled in MARCH3 version 192. However the effect of both phenomena on the heat transfer can be simulated properly by control of the auxiliary feedwater mass flow rate and hence, the heat transferred from the primary to secondary system. For this reason the auxiliary feedwater mass flow rate has been controlled in the analysis in such a way that the steam generator heat exchange in TMI-2 was followed as closely as possible. The recommended auxiliary feedwater rates of Anderson [2] were therefore not used in the MARCH3 calculation.

In MARCH3, the Reactor Coolant System (RCS) is modelled as a single volume. The PORV is connected to this volume at an arbitrarily selected elevation. discharging pure steam when the liquid level is below this elevation and discharging pure liquid when the liquid level is above this elevation. In order to simulate two-phase discharge during the TMI-2 accident, it has been decided to locate a PORV on top of the MARCH3 volume always discharging steam, and an artificial break at the bottom of the volume, always discharging liquid. For this reason a table has been included in the calculation model, containing PORV and break areas as function of time, taking into account when the PORV block valve was open or closed.

In MARCH3, a so called dead-end volume has to be defined connected to the vessel volume, in which water is trapped when the primary pumps are switched off. However the exact size of this dead-end volume is difficult to determine. For this reason the size of the break at the bottom was tuned in order to compute the core recovery at the right time in stead of adaption of the dead-end volume. For that reason the amount of liquid discharge through the - artificial - break does not represent the actual liquid discharge as fraction of the two-phase discharge through the PORV.

Summarizing, the following boundary conditions have been used in the MARCH3 calculation model:
- Steam generator secondary pressure of the A-loop.
- Transferred heat from primary to secondary system.
- Time of core uncoverage when core exit temperatures raise above saturation condition.
- Makeup flow rate.

Although these boundary conditions deviate from the ones which have been set up for the TMI-2 Accident Exercise, the use of these boundary conditions is required in order to obtain MARCH3 calculation results with any physical significance.

The reactor core is simulated in MARCH3 by a cylindrical body located at the bottom of the single volume. In the calculation model the core has been divided into 10 axial sections (the first one at the bottom of the core) and 10 radial rings (the first one at the center line of the core).

3. RESULTS

The sequence of events during the TMI-2 accident has been divided into four different phases. First the characteristic of each phase is described, followed by a discussion of the calculation results.

3.1 Phase 1, Loss of coolant (0 - 100 min.)

The accident was initiated by a total loss-of-feedwater to the steam generators. The resulting increase in the RCS pressure caused the FORV on the pressurizer to open and the reactor to scram. The FORV failed to close as the RCS pressure decreased. The reactor operators, misinterpreting the pressurizer liquid level (they thought data indicated a nearly full RCS), reduced emergency core cooling (water injection) while coolant continued to be lost from the FORV. A small-break LOCA with limited ECCS was underway. Because of decreasing liquid inventory in the primary cooling system and continued heating by decay heat in the fuel, coolant void fraction increased sufficiently over the next hour to cause the reactor pumps to vibrate excessively from cavitation. At 73 min., both pumps in the B-loop were shut-off, resulting in reduced flow in the B-loop and loss of heat removal by the B-loop steam generator. At 100 min., the A-loop pumps were shut off, terminating the forced convection cooling of the core. Prior to 100 min., forced two-phase flow through the reactor core provided sufficient cooling to prevent core heatup. After pump operation was terminated at 100 min., steam/water separation occurred within the RCS, and liquid from the top of the reactor vessel and the primary cooling system piping settled into the reactor vessel. The vessel's resulting liquid level is estimated to have been near the top of the core, shortly after the last coolant pumps shutdown.

Figure 2 shows a comparison between the measured pressure in the TMI-2 primary system and the pressure as calculated by MARCH3. As can be observed, the calculated pressure time history coincides closely the measured one. Following reactor scram the primary pressure decreases until the saturation pressure is reached. At TMI-2, it took 5.5 min. for the whole primary system to reach saturation.
Heat transfer from the primary to the secondary system is re-established by injection of auxiliary feedwater after 3 minutes. Due to cooling of the secondary side, the primary pressure decreases again towards a pressure which is slightly higher than the secondary pressure ($t = 24$ min.). From this time on, the primary pressure closely follows this secondary pressure.

At $t = 94$ min., the flow through the stuck-open PORV changes from a two-phase mixture into steam only. The resulting increase in volume flow rate being discharged results in a pressure below the saturation pressure for a period of 5 minutes.
3.2 Phase 2, Core heatup and initial melting (100 - 174 min.)

The liquid level in the reactor vessel decreased after 100 min. because makeup to the primary cooling system was insufficient to compensate for coolant loss through the PORV. Core uncoverly began shortly after 100 min., and temperatures at the top of the core started to increase. Studies to correlate the core liquid level with the response of the source range monitor indicate that by approximately 140 min., the core liquid level had dropped to about midcore. The upper regions of the core would have heated sufficiently (1100 to 1200 K) to result in cladding ballooning and rupture. Cladding failure and release of gaseous fission products were substantially by significant increases in containment radiation levels commencing at 139 min. At approximately the same time, the reactor operators recognized coolant was being lost through the faulty PORV and manually closed the pressurizer block valve upstream of the PORV, thus temporarily terminating coolant loss and the release of gaseous fission products to the containment. By 165 min., core cooling water probably dropped below 1 m. This drop would have resulted in downward, as well as radial, progression of core damage.

As shown in Figure 2, the RCS pressure increased = 5.5 MPa between 150 and 174 min. This increasing RCS pressure results primarily from the continued progression of core damage. Zircaloy cladding oxidation at temperatures above 1500 K was probably vigorous, producing significant quantities of hydrogen that subsequently degraded heat transfer in the steam generator to the secondary system, and also rapidly heated the core above temperatures required to melt control rods and fuel rod cladding. The degraded heat transfer from primary to secondary system, accompanied by rapidly rising temperatures within the core and high steam production associated with cooling of molten core materials at the liquid/steam interface, was the main cause of the rapid RCS pressure increase during this period.

The calculated core collapsed water level is depicted in Figure 3. As can be observed in the figure, core uncoverly begins at t = 118 min. From this time on, core exit temperatures increase. The level increases again after the PORV closes at t = 139 min. The calculated rate of core water level increase corresponds to a high pressure injection rate of 4 kg/s.

Some of the calculated core temperatures at representative locations are given in Figure 4; the first index of the temperature TC indicating the elevation, the second index the radial location. The maximum core temperature of 2873 K in the calculation occurs after 153 min. in the center of the core.

![Figure 3 Calculated water level in TMI-2 reactor core](image-url)
at 2.5 m elevation. This maximum temperature is an input variable of MARCH3 which is considered to be the melting temperature of the core. The value of this temperature has been arbitrarily selected. In the calculation the temperature where (U,Zr,O) liquid ceramics are formed, has been used as the maximum temperature. The melting temperature of UO$_2$ (3100 K) was considered too high, the temperature where α-Zr(O)/UO$_2$ and U/UO$_2$ monetecics are formed (± 2870 K) was considered too low. When the melting temperature is reached, MARCH3 assumes that a molten region is formed in the core which grows downward in such a manner that the average temperature of the region remains at the melting temperature.

The hydrogen production which accompanies the oxidation reaction is given in Figure 5. A total amount of 471 kg has been calculated which is close to the 459 kg which is estimated to have been produced during the accident. A significant difference between the code result and the estimated data is that MARCH3 predicts the termination of the hydrogen production directly after the pump transient while expert opinions state that half the amount of hydrogen was generated after the pump transient during the continuation of the degraded core heatup.

A parametric variation of the core melting temperature results in 561 and 255 kg hydrogen production for melting temperatures of respectively 2670 and 3100 K.

3.3 Phase 3, B-loop pump transient and degraded core heatup (174 - 200 min.)

Activation of the 2B coolant pump at 174 min. resulted in the first significant addition of coolant to the reactor vessel following the A-loop pump shutdown at 100 min. The 2B pump was allowed to operate for ± 19 min. However, significant flow in the B-loop hot leg was measured only the first 15 seconds. Approximately 28 m$^3$ of water may have been pumped into the reactor vessel. The RCS experienced a rapid repressurization as a result of the pump transient, as shown in Figure 2.

Following the B-pump transient, the core liquid level decreased as decay heat from the degraded core boiled liquid from the reactor vessel. The liquid
level above the bottom of the core at 200 min. was equal to 
\( \pm 2 \text{ m} \). Evaluation of RCS steam production from 192 to 197 min. 
indicated that heat transfer from the core was less than 
nominal decay power, and thus consistent with measurements, 
indicating continued core heatup during this period.

In the MARCH3 code, no model is present to describe the 
forced coolant flow through the reactor core. Therefore, the 
B-pump transient has been simulated in the calculation by 
a temporary operating ECCS pump. Disadvantage of this method is 
that relatively cold water is injected into the reactor vessel 
instead of water at saturation temperature. As a consequence 
of this cold water injection, the effect of the B-pump 
transient is overestimated. 
As can be observed in Figure 3, 
the reactor core is completely 
covered after \( t = 174 \text{ min} \). 
Continuation of degraded core 
heatup is not calculated in the 
period following the B-pump 
transient. Figure 3 shows only 
a very small re-coverage after 
180 min. which does not affect 
a further cooldown of the 
reactor core. Figure 4, shows 
that the calculated core tempera-
atures decrease until reactor 
cooling water temperatures are 
reached. The MARCH3 code permits 
refreezing of moltenmaterial 
following injection of water. 
In the calculation all molten 
material refreezes directly when 
these cooling water temperatures 
are reached.

The only comparison of 
calculated data can be made with 
the temperatures measured in 
the hot legs of the A- and 
B-Loop. However, these parti-
cular temperatures are not 
calculated by the MARCH3 code. 
The closest MARCH3 temperatures 

![Figure 6](https://via.placeholder.com/150)

**Figure 6** Comparison between calculated and measured system 
temperatures.

are the temperatures of the 
upper plenum and piping 
structure on the reactor vessel. 
Calculated and measured tempera-
tures are shown in Figure 6. 
Due to data reduction of the 
measured hot leg temperatures, 
some detail of these tempera-
tures has been lost. At the end 
of phase 3 the temperatures 
coincide more or less but there 
is no evidence that this trend 
is indicative for phase 4.

3.4 Phase 4, Core relocation and 
recovery period (200 - 300 
min.)

At 200 minutes, the HPI 
system was actuated and 
emergency cooling water was 
injected for the next \( \pm 17 \text{ min} \). 
Analysis of the RCS pressure and 
temperature data indicate that 
the reactor was refilled with 
water by \( \pm 207 \text{ min} \). A signifi-
cant source of water to the 
coolant inventory in the reactor 
vessel came from drainage of the 
pressurizer coolant as RCS 
pressure decreased.

As emergency cooling water 
filled the reactor vessel, water 
began to penetrate the upper 
debris bed, cooling and 
eventually quenching the debris 
bed. Subsequent cooldown and
quenching of the debris bed are estimated to have taken ± 25 min. Thus, final quenching of the debris bed is estimated to have occurred after ± 230 min. Relocation of ± 20 tonnes of molten corium into the lower plenum of the reactor vessel occurred at ± 224 min. after reactor scram. This has been indicated by RCS pressure, cold-leg temperatures and reactor instrumentation. The molten corium rapidly settled into the reactor vessel lower head and was probably not cooled significantly by water during the period of relocation. The RCS pressure pulse between 224 and 240 min., shown in Figure 2, implies that heat transfer and steam generation within the debris bed was significant for at least 15 min.

In attempting to establish RCS flow during this period, the pressurizer block valve was repeatedly cycled, resulting in significant RCS coolant transfer to the containment building. Most transfer of water to the containment from the RCS occurred after the major core relocation at 224 min.

Figure 7 Calculated fraction of core melted and fraction of clad reacted.

It has been the intention to analyze also phase 4 with MARCH3. Although this calculation has been made, it was abandoned because MARCH3 predicts that the accident is terminated after the B-pump transient. This termination is elucidated in Figure 7, showing the fraction of core material being molten. MARCH3 calculates that 51% of the core would melt which refreezes after the pump transient at 174 min. This value is in rather good agreement with the post-examination of the reactor core which concludes that 45% of the core has been molten. Figure 7 also presents the fraction of the zircaloy cladding which is calculated to be actually involved in the oxidation reaction. This percentage amounts to 47%. From these percentages it can be concluded that the quantitative results of MARCH3 are within reasonable limits. The timing of the core damage progression however, does not agree with expert opinion saying that approximately half of the core degradation occurred following the B-pump transient.

A parametric variation of the core melting temperature results in different fractions of molten core material. For a lower melting temperature of 2670 K, 60% of the core is calculated to be molten. Only a few percent is calculated to be molten when a melting temperature of 3100 K, being the melting temperatures of UO₂, is used.

4. SIGNIFICANCE OF RESULTS

The TMI-2 accident sequence of events has been analyzed using the standard MARCH3 version 192 code as present in the US-NRC’s Source Term Code Package. The phases 1 through 3 of the accident have been
calculated after the implementation of a code modification in the steam generator heat transfer model, as described in paragraph 2 above.

A significant result of the TMI-2 analysis exercise is that the thermohydraulics of the system, being analyzed with the MARCH3 code, should be known in advance. When these thermohydraulics are known, either measured or calculated by means of a more detailed mechanistic code, they can be used in MARCH3 analysis as boundary condition. The boundary conditions for the TMI-2 Analysis Exercise have been set up in such a way that the measured thermohydraulic behavior was reasonably calculated.

With the thermohydraulics correctly in shape, the (end) user is still confronted with a large amount of input parameters, giving him a large degree of freedom to model the process under consideration. The MARCH3 analysis has predicted a core degradation behavior which was close to what has happened during the TMI-2 accident: 51% core melt and 471 kg hydrogen generation calculated compared with 45% core melt and 459 kg hydrogen derived from post-accident investigation. By variation of the core melting temperature, the exact values would probably have been achieved. However, the time when the core degradation is terminated does not coincide the actual value: molten core material has relocated after 224 min, while MARCH3 predicts refreezing of the complete melt directly after the pump transient at 174 min. The hydrogen generation therefore stops at 174 min instead of a gradual decrease. However, the effect of the E-pump transient may be over-predicted due to the simulation of this transient in the calculation by the actuation of an extra ECCS pump.

4. CONCLUSIONS

From the participation in the TMI-2 Analysis Exercise and the study with STCP's MARCH3 code, it can be concluded that:

- Participation has been very useful to understand physical phenomena which occurred during the TMI-2 accident. Data of TMI-2 have been used for the evaluation of feasible source terms in the Netherlands.

- The use of MARCH3 version 192 as an analytical tool has clearly shown the limitations of the code: the thermohydraulic models of MARCH3 are too much simplified for detailed analysis of complicated accident sequences such as TMI-2.

- The standard MARCH3 version 192 code requires modification of the steam generator heat exchange model. However, the MARCH3 code can still be used for parameter studies in post-accident analysis when the thermohydraulic system behavior is known and adequately simulated in the input.
5. REFERENCES.


Appendix F

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SENSITIVITY ANALYSES ON TMI-2 ACCIDENT

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- 1 -

SENSITIVITY ANALYSES ON TMI-2 ACCIDENT

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ABSTRACT

Sensitivity analyses of TMI-2 sequence up to the beginning of the accident recovery, performed using SCDAP and RELAP/SCDAP computer codes, are presented in this paper.

The main purpose was to investigate the relevance of some modelling choices considered important in understanding the accident evolution. In particular, the influence of the modelling of fuel melting and molten core material relocation on the hydrogen generation and core thermal response is discussed.

The effect of noncondensable gases on the circuit fluid-dynamic was modelled to verify its influence on natural circulation.

SOMMARIO

Nel presente lavoro sono presentati i risultati di studi parametrici sulla seconda fase della sequenza incidentale di TMI-2 effettuati con i codici di calcolo SCDAP e RELAP/SCDAP.

Scopo primario dello studio è testare e qualificare alcuni modelli per indagare le fenomenologie ancora non sufficientemente note che si sono verificate durante l'incidente. In particolare vengono indagati i fenomeni connessi con la fusione della barretta di combustibile e con la rilocalizzazione del materiale fuso, ed il loro effetto sulla valutazione realistica della produzione di idrogeno e del transitorio termico del core.

Una nodalizzazione dettagliata del vessel ha permesso di verificare l'importanza dei fenomeni di circolazione naturale nonché di stimare l'effetto della presenza di incondensabili nel circuito.
INTRODUCTION

The TMI-2 accident has been a unique invaluable source of information to improve the understanding of core damage and source term phenomena and the knowledge on severe accident evolution in an operating Pressurized Water Reactor (PWR).

The plant recovery has required several years of efforts and millions of dollars to be completed; this has been possible through a research program sponsored by US-NRC with the contributions of some OECD countries.

The aim of the research has been to get information on the plant thermal-hydraulic behaviour; besides the core examination provided a significant data base on the governing phenomena in the early and late phases of a core melt progression, including the material relocation processes.

In addition to this research, the capability of modelling the TMI-2 accident has been improved, together with mechanistic models to threat complex phenomena expected during severe accidents. This analytical activity has helped to better understand the experimental data and to provide realistic prediction of the scenario evolution during a core damage sequence.

The TMI-2 accident evolution was divided, according to the main anticipated phenomena, into four major time periods:

  - the loss of coolant period with the main circulation pumps on, from 0 to 100 minutes (phase 1);
  - the initial core heat-up and degradation period, from 100 to 174 minutes (phase 2);
  - the degraded core heat-up period, from 174 to 224 minutes (phase 3);
  - the period of core relocation, from 224 to 230 minutes (phase 4).

So far, the international standard problem promoted by OECD-CSI on the accident simulation has concerned up to now the first two phases of the above described scenario [1]. The objectives include the understanding of what really happened during the accident and the use of the TMI-2 data to benchmark severe accident analysis codes.

The aim of the present activity, carried out by ENEA-DISP and Pisa University, is to verify the assessment state of RELAP/SCDAP, that is an integrated computer code potentially able to predict overall severe accidents behaviour, but which needs a suitable user strategy to give good results, mainly because of the interactions between thermal-hydraulic and fuel models.

The effects of hydraulic boundary conditions are very important on core damage prediction and require an extreme accuracy of the models simulating the thermal-hydraulic phenomena which, in turn, drive the thermal-mechanical and chemical-physical processes.

The sensitivity calculations require very long computational time that represents a limitation in using the code. For this reason, analyses of the second phase of TMI-2 accident scenario have been carried out using both SCDAP and RELAP/SCDAP computer codes to check the capability of core damage models: first when boundary conditions are fixed by engineering judgement criteria (SCDAP), and then when they are linked with thermal-hydraulic models in an integrated system code (RELAP/SCDAP).

Specifically, items arising from core failure, molten material relocation and the effects of noncondensable gases on the system thermal-hydraulic will be discussed.
TMI-2 ACCIDENT SCENARIO

The accident was initiated by a partially unrecovered loss of feedwater to the steam generators, resulting in a turbine trip. Subsequent reduction of the heat removal from the primary cooling system resulted in an overpressurization, causing the power operated relief valve (PORV) to open and the reactor trip [2]. With the reduced core power, system pressure decreased below the PORV closure set point. But the valve did not close causing essentially a Small Break LOCA (SB-LOCA) for more than 2 hours. The decrease in fluid mass inventory and in reactor thermal power resulted in an initial drop of the pressurizer level, followed by its sudden increase to off-scale value when the steam generators heat removal degraded. The coolant void fraction in RCS increased continuously, due to the mass loss from the open PORV, and caused the coolant pumps to experience excessive cavitation.

At about 73 minutes, both pumps in the B-loop were switched off and then at 100 minutes the A-loop pumps were stopped too, then terminating forced convection cooling of the core.

The homogeneous two-phase mixture in the RCS stratified at level close to the top of the bundles. Core heat-up started with extensive oxidation, melting and relocation of zircaloy cladding and UO₂ until 174 minutes. At this time one of the two B-loop pumps was turned on injecting coolant into the reactor vessel.

Interaction of the coolant with the oxidized and embrittled upper fuel rod sections resulted in a debris bed whose surface, cooled by this mass flow transient, formed a solidified crust which held up the upper core debris, while its interior was not appreciably cooled, due to its large size and to the low thermal diffusivity of the consolidated region.

At 200 minutes emergency core coolant injection was initiated and, within few minutes, the vessel had been filled with water. Anyway poor cooling of consolidated region occurred until 224 minutes when, based on the response of the in core instruments, the crust containing the molten core material failed, and a significant core relocation in the lower plenum occurred.

CALCULATION RESULTS

SCDAP [3,4] and RELAP/SCDAP [5] computer codes have been used in the analyses of the second phase of the TMI-2 accident. During this phase, the interest is in predicting the core liquid inventory, the core heating, the hydrogen production, cladding and fuel melting and relocation.

In particular, the SCDAP sensitivity analysis investigates the quenching effect of liquified material on the core thermal response. This study has been undertaken to define the range of the core damage parameters, to be used in the final RELAP/SCDAP calculations.

The TMI-2 core is modelled by one "control rod", one "thermal shroud" and three "fuel rod" components arranged as shown in Fig. 1. The input deck used the plant data as defined by the dataset from EG&G [1].
The core damage simulation was started with conditions corresponding to the reactor state at 100 minutes which are characterized by the following assumptions:
- the water level at the upper core (3.66 m);
- the saturated water at 64 bar;
- the initial pressure in the fuel rod gap at 56 bar;
- the power distribution taken from NRC literature [1].

With regard to the time dependent boundary conditions, enthalpy mass flow rate and pressure history were taken from ref. [6], due to the lack of experimental data for the coolant flow rate into the reactor vessel during the accident. Sensitivity analyses were performed scaling down the reference value of the mass flow rate at the core inlet.

Figures 2 and 3 show the calculated peak cladding temperature and core mixture level. We note that in the base calculation, predicted core temperature rise is small, mainly due to the over estimate of the early cooling promoted by the quenching of the molten control rod material flowing down in the core channels, so that only a limited core damage is predicted at the end of the simulation. Results of sensitivity studies indicate a dramatic effect of the assumed mass flow rate on the core thermal response: reduction to 25% of the engineering best estimate value, changes completely the accident scenario evolution [7]. From Figure 2 the core heat-up is only a little perturbed by the mentioned precooling effect at about 150 minutes and the core peak temperatures exceed the damage values within the time windows until 174 minutes.

Besides, it must be noted that only 1 Kg/s is the difference between the reference (1.6 Kg/s) and the 75% reduced (0.4 Kg/s) flow rate through the whole reactor core, whose nominal flow is about 17000 Kg/s.

Therefore, it is immediately apparent that TMI-2 accident evolved with thermal-hydraulic conditions whose required accuracy in prediction is really prohibitive if compared with the uncertainties in the plant boundary conditions. Moreover, these results bring out that at temperatures much lower than the fuel rod melting or liquefaction point, control rod material relocation become very important in the scenario evolution.

Present model in SCDAP assumes that, when failure occurs in silver-indium-cadmium control rods, the molten cadmium rapidly vaporizes and condenses into an aerosol, if cooled outside the core, while the molten silver and indium relocate downward to freeze in the colder regions of the core. If this relocated hot control rod material falls into the water, it causes significant steam generation, that SCDAP calculates simply by:

\[ \dot{U} = \frac{G_{fg}}{\Delta t \ H_{fg}} \]

where \( G_{fg} \) is the steam generation rate, \( U \) is the integrated heat capacity of the liquified control rod material from initial conditions to saturation temperature with the water, \( H_{fg} \) is the latent heat of vaporization and \( \Delta t \) is the quench time interval.

Clearly in the prediction of the steam generation rate, \( \Delta t \) is a critical parameter which is connected to the geometry of the dropping molten control rod material as to the heat transfer regime which establishes in the falling region.
On the basis of the experimental data obtained from PBF tests in the frame of NRC-SFD program, $\Delta t = 50$ seconds has been fixed in the code.

To check the sensitivity to the "quenching time", the base case calculation was repeated with that value increased to 500 seconds. The resulting core components temperature and core mixture level histories are shown in Figs. 4 and 5. As it is immediately apparent, there is significantly higher heat-up than in the base case calculation and the predicted core damage status at 174 minutes, just before the B-loop pump transient, well agrees with engineering judgment based evaluations.

The total amount of liquified UO$_2$ was calculated to be 6700 Kg; not all the eutectic is predicted to freeze within the lowest axial node, but some 30% dropped below the bundles. Anyway a rod like structure is calculated in the higher elevation of the core.

From figure 6 it can be seen that the amount of oxidation is considerably increased too. This is particularly important with reference to the circuit fluid-dynamic.

In fact, calculation predicts about 6 kg of hydrogen is generated up to 144 minutes, when auxiliary feedwater in B loop steam generator was restarted.

That amount of hydrogen is enough to fill the upper section of the circuit, steam generator tubes included, and then to limit the steam condensation driven by the heat sink recovery.

Underestimation of this effect could be one of the reasons of RELAP/SCDAP code failure in a previous integrated vessel-circuit simulation [8]. In this last case, excessive condensation in steam generator tubes may occurs, due to a poor hydrogen generation prediction, that may result in the pressurizer emptying with a premature core recovery due to the movement in the vessel of a remarkable water mass inventory (about 40 m$^3$).

Thus it is clear that hydrogen generation is another critical parameter in the accident simulation.

Oxidation of the zircaloy by steam becomes important when cladding temperatures exceeds about 1200 °K.

The heat production by the exothermic reaction:

$$\text{Zr} + 2\text{H}_2\text{O} \rightarrow \text{ZrO}_2 + 2\text{H}_2 + 6500 \text{ kJ/kg}$$

provides additional temperature increase which results in a rapid autocatalytic oxidation when ZrO$_2$ experiences tetragonal to cubic phase transition, if the process is not limited by conversion into hydrogen of all the steam (steam starvation).

SCDAP oxidation model uses Catchart-Pawel rate constant below 1800 °K and Urbanic data above the related transition phase [9].

Anyway considerable uncertainties in hydrogen generation were expected to come from loss of cladding integrity, zircaloy melting and fuel dissolution and relocation.

In particular the burst of the cladding, which allows some steam to penetrate through the breach into the fuel rod gap, starts a double sided zircaloy oxidation resulting in a larger metal-water reaction and in a different distribution of base and oxidized zircaloy through the cladding thickness. But SCDAP code is not suitable to take into account all the effects related to this phenomenon.
For this reason, starting from previous SCDAP simulations, vessel calculations were performed by integrated RELAP/SCDAP computer code that, in addition to the above described effects, allows to accurately describe a thermal-hydraulic system with SCDAP models simulating the core components. Same initial conditions as in SCDAP base case calculation were assumed. As regards to the boundary conditions the system pressure was fixed by a time dependent volume connected to the hot leg and the inlet mass flow rate from the cold leg nozzles is adjusted by a time dependent junction in order to agree the core level with a reference best estimate trend [6].

Results from this calculation confirmed the extreme sensitivity of the core performance to the boundary conditions. Besides this calculation helped to better understand the scenario evolution from a thermal-hydraulic point of view. In fact on the one hand modelling three different core channels, downcomer, core bypass, lower and upper plenum and lower and upper head, is surely more realistic that one channel alone of SCDAP to simulate the flow path in the vessel, on the other the obtained results justify the use of simpler but faster (and then less expensive) analytical tools for the approach to the scenario comprehension.

Finally, a lack in RELAP/SCDAP computer code has been evidenced by the calculation difficulties coming from synergism among a very high number of phenomena. The introduction in RELAP/SCDAP of an internal cladding oxidation model solved the above mentioned SCDAP inadequacy (after cladding rupture) from the metal-water reaction point of view but adds a new problem with regard to the cladding-fuel interaction simulation.

In fact, the presence of a fast zircaloy oxidation on the inner cladding surface can prevent the molten zircaloy to dissolve any uranium oxide (Fig.7): in this way no liquefaction occurs when double sided oxidation is predicted by the code, and fuel melting remain the only in-vessel fuel relocation process. So, last vessel simulation predicts no relocation to happen until 174 minutes. This is clearly a code fault and needs a model upgrading.

CONCLUSIONS

Second phase of the TMI-2 accident has been modelled and simulated with SCDAP and RELAP/SCDAP codes. Results of performed sensitivity analyses provided useful information both to the core degradation phenomena understanding and to the analytical model and computer code assessment.

With reference to the scenario evolution, it comes out that TMI-2 accident evolved within very particular plant conditions and that the core damage severity developed only in such a situation. A little different conditions, probably, would result in an early accident recovery with only a limited core damage.

Fuel cladding interaction and metal water reaction had surely a very important role because of their feedback on the circuit thermal-hydraulic behaviour (noncondensable gases in steam generator tubes, changes in the hydraulic geometry of the core).

Melting and relocation of silver-indium-cadmium control rods are other important aspects of the scenario evolution that occurred during the second phase of the accident, and whose comprehension will help to better understand the next phases, including what happened in the lower head of the vessel.
With reference to the code assessment and code strategy activities, lessons learned from separate (SCDAP) and integrated (RELAP/SCDAP) computer codes were obtained.

As regards to the SCDAP, notwithstanding its very simple thermal-hydraulic representation, it presents a quite good agreement with RELAP/SCDAP results as long as simulation lasts.

On the other hand the extreme sensitivity of fuel damage models with respect to the thermal-hydraulic conditions, requires that sensitivity calculations be performed. RELAP/SCDAP code is unsuitable for this aim, because it is very expensive, instead SCDAP code, because its low CPU time request, allows to make it with a reasonable cost-results balance.

From the modelling point of view, criticality of metal-water reaction feedback (in particular double sided oxidation and oxidation under multilayer configuration) on liquefaction and relocation processes has been outlined. Quenching model of control rod material has been also discussed. Problems related to scaling-down effects of in-pile tests with respect to a full-scale reactor have come out.

Close link among a high number of damage phenomena is expected to be checked; improvements in a model (double sided oxidation) can have unfavourable effects on other ones (fuel liquefaction). As final conclusions:

- thermo-chemical and thermo-mechanical models give generally good results if thermal-hydraulic conditions are well predicted;
- thermal-hydraulic boundary conditions require a high level of predictive accuracy during very low water inventory scenario, like TMI-2 accident;
- oxidation and relocation models need further improvements to take into account synergetic effects resulting from late phase of core degradation;
- special damage phenomena are sensitive to the scaling-down effects and request proper investigation;
- RELAP/SCDAP integrated code is generally able to analyze the core damage progression during a severe accident but the computational costs limit its utilization to already clear accident sequences;
- SCDAP stand alone computer code remains an important analytical tool to investigate core damage phenomenology; anyway improvements of thermal-hydraulic model are wished in order to extend its application to a larger number of fluid-dynamic regimes, now limited to pool boiling conditions.

**ABBREVIATIONS**

CSNI  Committee on Safety of Nuclear Installations
DISP  Direzione Sicurezza Nucleare e Protezione Sanitaria
ENEA  Comitato Nazionale per lo Sviluppo dell'Energia Nucleare e delle Energie Alternative
EPRI  Electric Power Research Institute
LOCA  Loss Of Coolant Accident
NRC  Nuclear Regulatory Commission
OECD  Organization for Economic Cooperation and Development
PBF  Power Burst Facility
PORV  Power Operated Relief Valve
PWR  Prezzurized Water Reactor
REFERENCES


- Total number of components: 5

- Subdivision of the core in 3 fuel regions:
  Region 2 (2): 72 Bundles (14976 pins)  "  "  "  1.076
  Region 3 (3): 80 Bundles (16640 pins)  "  "  "  0.859

- 1 Thermal Shroud component (4)
- 1 Control Rod component (not shown)
- Number of axial nodes: 10 (having the same height).

Fig. 1 - Main features of SCDAP nodalization
Fig. 2 – Peak cladding temperature: sensitivity to the core mass flow rate

Fig. 3 – Core mixture level: sensitivity to the core mass flow rate
Fig. 4 - Peak cladding temperature: sensitivity to the quenching time

Fig. 5 - Core mixture level: sensitivity to the quenching time
Fig. 6 - Hydrogen generation: sensitivity to the quenching time

Fig. 7 - Fuel rod after the cladding burst
Appendix G

Fauske & Associates, Inc.
TMI-2 ANALYSIS EXERCISE
FINAL REPORT

MAAP CODE DESCRIPTION AND ANALYSIS
REPORT
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I. INTRODUCTION

As part of the TMI-2 Analysis Exercise sponsored by the Organization for Economic Cooperation and Development Nuclear Energy Agency, the first 300 minutes of the TMI-2 accident (Phases 1 through 4) have been simulated using both the MAAP (Modular Accident Analysis Program) 3.0B and MAAP-DOE code versions. MAAP 3.0B is a fast running, integrated severe accident code developed by the Industry Degraded Core Rulemaking Program (IDCOR) to analyze the five IDCOR reference plants. MAAP 3.0B simulates an accident transient and specifically accounts for system events that occur during the transient, including operator actions, until a permanently coolable state is achieved or until the containment has failed and depressurized. Models are included for all the important phenomena that might occur during accident sequences leading to degraded cores. The DOE version of the code, developed for the United States Department of Energy, has improved models for degraded core recovery and core material interaction that allow simulation of Phases 3 and 4, but is otherwise the same as MAAP 3.0B. Brief consideration of both codes is included in the code description appendix to this report. The code description appendix illustrates the philosophy behind the MAAP code package and highlights the differences between the 3.0B and DOE degraded core models.

To use either code version, a parameter file describing plant geometry, system performance, controls and initial conditions, and a set of control cards, called an input deck, are required. A parameter file based upon data supplied as part of the TMI-2 Analysis Exercise package, was constructed for both versions. Parameter files for 3.0B and DOE versions are essentially similar, with the DOE parameter file requiring a few more modeling parameters, namely heat transfer coefficients, porosities, and the like, for the degraded core recovery model. An input deck is used to define boundary conditions and operator actions during the accident. Although both code versions have a secondary side model, plant data for the secondary side must be used as boundary conditions for the primary system since auxiliary
feedwater flowrates, a boundary condition for the secondary side, were not recorded during the accident. An input deck was created based upon the analysis exercise package to replicate the secondary side conditions, mainly steam generator levels and pressures, and simulate operator actions during the accident.\(^{(3,4,5)}\)

Results and conclusions will be presented for both code versions. For Phase 1, only the MAAP 3.0B results are considered since the 3.0B and DOE versions give identical results prior to core damage. Results for both codes are presented for Phase 2 because core damage begins during this phase. These results characterize differences between the codes in their core melt progression and hydrogen generation models. Results for Phases 3 and 4 are presented for MAAP-DOE only. MAAP 3.0B lacks the degraded core recovery and enhanced core material interaction models needed to simulate Phases 3 and 4 of the accident; MAAP-DOE results for the Phases 3 and 4 prove to be much more successful. For both code versions, and all phases of the accident, results for the secondary system are presented to show that the input deck provides acceptable results for steam generator levels and pressures, and thereby gives the boundary conditions for the primary system simulation. Results for the primary system are then compared to the available plant data.

II. RESULTS

A. Node Diagram

Nodalization of the primary system, reactor vessel, and core for the TMI-2 accident simulation is presented before discussing code results. This discussion is meant to illustrate code philosophy and principles rather than provide detailed description of any particular MAAP model ("MAAP" by itself is used to denote either code version).
Since containment integrity is the focus of the MAAP code, the primary system has been designed largely to predict the driving function of the containment response. The thermal-hydraulics models for the primary system are simplified, but the primary system model does calculate the in-vessel generation rates for hydrogen, molten core material, fission products, and the release to the containment of hydrogen, steam, and molten core material. To predict the release rate to the containment, the primary system model must predict gas and structure temperatures in the primary system, as well as the flows between the primary system and containment. As a result, the following primary side quantities are tracked:

1. primary system pressure,
2. materials temperatures,
3. void fraction of coolant at the exit points of the primary system,
4. hydrogen concentration and fission product loading of gas at these points,
5. water masses in the primary system sub-volumes, and,
6. forced and natural circulation flows within the primary system.

The MAAP primary system nodalization for tracking these quantities in a Babcock & Wilcox type plant is shown by Figure 1. In MAAP, all loops except one, the broken loop, are lumped together, and the broken loop is treated separately; "broken loop" refers to the loop that can contain a primary system break. The user selects whether the pressurizer is in the broken or unbroken loop. For the TMI-2 accident the A-loop is the broken loop.

Each node has an individual gas temperature, hydrogen mass fraction, and up to four structure temperatures. Water pools are separately accounted for in the unbroken and broken cold leg tubes and intermediate cold legs, and in the downcomer node. The downcomer water pool includes water in the horizontal part of the cold legs running from the reactor vessel to the pumps. Therefore, the downcomer water volume encompasses several gas nodes. This was done because the primary focus of the code is the prediction of thermal hydraulic conditions and fission product transport, and more detail is required for gas transport processes than for water pools. All water not
Figure 1. B & W primary system nodalization.
contained in the cold sides of the primary loops and the downcomer is treated as the core water pool. After the other nodes have been considered, the mass of water in the hot legs is calculated algebraically from the total core water pool mass.

The reactor vessel nodalization, in the form of heat sinks and control volumes, is shown by Figure 2. For the core region, the user specifies the number of radial rings and axial rows used. A maximum total of 70 nodes is allowed, with a maximum of 7 rings and a maximum of 20 rows. For example, 7 rings by 10 rows, or 3 rings by 20 rows, is acceptable, but 5 rings by 16 rows, or 8 rings by 8 rows, is not. For each ring, a radial peaking factor and volume fraction are specified by the user, and for each row, an axial peaking factor is specified by the user. For simulation of the TMI-2 accident, 4 rings and 15 rows were used to nodalize the core.

Mass and energy rate of change for core materials are calculated for each core node. Steam and hydrogen are assumed to flow along the uncovered and unblocked flow channels, and the mass flow rates and enthalpies in each channel are calculated by tracking the generation and consumption at each axial level. The core water pool is treated as a lumped mass and energy control volume.

B. Discussion of Results

Phases 1 and 2 - MAAP 3.0B

Code results for the secondary system are presented prior to comparison of primary system results with plant data. Boundary conditions for the primary system were derived from the reactor data as provided by the AE data base. These include:

1. the secondary side water level in steam generators A and B;
2. selection of several dump valve settings to closely simulate the secondary side pressure in both steam generators.
Figure 2. MAAP reactor vessel nodalization.
The secondary side water level is calculated in MAAP by specifying the desired level and the level controller parameters such as a dead-band level and the maximum flow rate of the auxiliary feedwater flow. The resulting water levels and the integrated auxiliary feedwater flow are shown in Figures 3 and 4 for the A-loop steam generator (SG) and in Figures 5 and 6 for the B-loop SG. Generally, the auxiliary flow calculated by MAAP is in agreement with that estimated in the AE data base also shown in Figures 4 and 6. These estimates in the AE data base were carried out using RELAP and a thermodynamic analysis of the secondary side based on the measured level and pressure of each of the steam generators.

The secondary side pressures were approximately equal to the secondary side relief valve set point during most of the first 60 minutes of the accident. After that time, the atmospheric dump valves were opened and used to control the secondary side pressures. This is simulated in MAAP by allowing the effective valve opening area for each steam generator to be changed at few selected times between 60 and 174 minutes. Comparisons between the calculated pressures and data are shown in Figures 7 and 8 for the A and B steam generators, respectively. For the A-loop SG a higher pressure is calculated between 140 and 160 minutes. This can be corrected by increasing the dump valve opening area in the A-loop side during that time. Due to the limited heat transfer across the tubes of this steam generator during that period, a small effect on the primary system behavior is expected. The calculated pressure in the B-loop decreases faster than the data after 73 minutes, when the B-loop pumps were tripped. Also, correctly simulating the water level after 94 minutes required the pressure in the B-loop SG to flatten out at 94 minutes, and therefore, to remain high during the rest of the transient. Improved modeling of the heat transfer to the SG secondary side, when its primary loop is stagnant, would improve the ability to simultaneously calculate the correct level and pressure in the B-loop steam generator. Note, however, that since the B-loop SG was isolated between 102 and 153 minutes it did not contribute significantly to the primary system behavior during that time. Hence the pressure discrepancy in Figure 8 has only a very minor effect on the simulation results.
Figure 3. Water level history in the A-loop generator as used in this simulation.
Figure 4. Integrated auxiliary feedwater mass history to the A-loop steam generator compared with EG&G RELAP analysis.
Figure 6. Integrated auxiliary feedwater mass history to the B-loop steam generator compared with EG&G RELAP analysis.
Figure 7. Calculated and measured secondary side pressures in the A-loop.
Figure 8. Calculated and measured secondary side pressures in the B-loop.
Having demonstrated the boundary conditions for the primary system, plant data for the primary system can be compared to code results. The following data can be used for comparison with predictions:

- A-loop flow rate,
- Temperatures in the A and B loops hot legs,
- Temperatures in the A and B loops cold legs,
- Pressurizer level, and
- Primary system pressure.

**A-loop Flow**

The two-phase flow rate of a main coolant pump is calculated in MAAP assuming that the delivered volumetric flow is constant. Therefore

\[
W_1 = W_0 \frac{\rho_1}{\rho_0} (1 - \alpha)
\]  

(1)

where \(W\) and \(\rho\) are the mass flow and density, \(\alpha\) is the coolant void fraction and subscripts 0 and 1 refer to the nominal operating conditions and to the liquid portion of two-phase flow, respectively. Since the liquid in the system was saturated and the pressure between 20 to 100 minutes was almost constant, equation (1) implies that the loop flow rate is a direct measure of the loop void fraction.

Comparison between the calculated flow and the data is shown in Figure 9. The good agreement reflects on the accuracy of the primary system mass balance, which includes the operator controlled makeup and letdown flows discussed previously and the coolant losses through the stuck open PORV. Deviations are noted between 8 and 22 minutes and after 73 minutes. The first time interval is when the steam generators were dry and the primary system pressure changed rapidly. This may suggest that equation (1) can deviate by about 10% from data for a rapidly changing pressure. This effect may also contribute to the deviations after 73 minutes. However, it seems likely that at that time, the calculated coolant distribution in the primary
System is not accurate enough. At 73 minutes the last B-loop pump tripped so that the A-loop was recirculating while the B-loop was idled. Under these asymmetric conditions MAAP assumes that a stagnant liquid column of a height equal to the homogeneous flow frictional head loss in the core exists in the idled loop cold legs. This assumption may have led to a larger calculated water inventory in the B-loop and hence to a higher void fraction in the A-loop. For accident analysis purposes, however, the overall agreement with the data, as demonstrated in Figure 9, indicates sufficient modeling adequacy for most cases of interest.

**Hot Leg Temperatures**

Temperatures in the A and B loop hot legs were measured by resistance temperature detectors (RTDs) and recorded by the reagentimeter during the accident. These data both provide an indication of the time at which the core began to uncover and measure the gas and structure temperature in the hot legs as the core temperatures increased. Since the RTDs measure some weighted average of the gas and pipe wall temperatures, the data comparisons are made to the calculated gas and hot leg pipe. The results are shown in Figures 10 and 11 for the A and B loop, respectively.

The A-loop RTD indicated a temperature rise by 112 minutes which suggests that the core probably started to uncover and produce superheated steam at about 111 minutes. This is in agreement with the MAAP calculated time discussed in the next section. While the calculated gas temperatures in the hot legs were much higher than the data, the calculated pipe temperatures follow the data closely until about 150 minutes. After that time the calculated pipe temperatures increase to approximately 1000°F (810 K). The data on the other hand, shows a rather sharp decrease in the heatup rate in both hot legs between 150 to 160 minutes with maximum indicated temperatures of 800°F (700 K). This decrease in the heatup rate of the hot legs when the core was hot and probably melting (see Figure 17) is not predicted by MAAP in spite of the modeling assumption that prevents gas recirculation through relocating molten core regions. Such an assumption on recirculation leads to
Figure 9. Comparison between the calculated flow in the A-loop and data.
some decrease in the gas space heatup rate in the upper plenum and the hot legs in this time period, but both the gas and metal structure temperature continues to increase to temperatures well above those measured. Hence, the comparisons in Figures 10 and 11 indicate that MAAP overestimates the heatup of the hot legs at times when the core is melting - possibly an indication that the modeling of natural circulation between the RPV and the hot legs overstates the efficiency of this process.

Another observation worth noting with regards to the calculated gas temperatures is illustrated in Figure 11, where the calculated B-loop hot leg gas temperature heats up rapidly after 106 minutes. At that time the core was presumably still covered (the calculated time at which the core started to uncover is 111 minutes, which is in agreement with the data in Figure 10). This behavior was not observed with previous MAAP versions e.g.,(6), and can not be explained physically. It probably reflects on some shortcomings in the algorithm that calculates the gas space temperature in this stagnant loop before the core uncovered.

After the core started to uncover the calculated gas temperature decreases and is later increasing at a reasonable rate. The algorithm for calculating the gas temperature involves several enhancements in MAAP 3.0B such as gas-to-structure heat transfer, two-dimensional nodalization of the heat structures, natural circulation of gas in the primary system and improved thermodynamic relationships. The exact reason for the unusual gas temperature calculation just prior to core uncovering needs further investigation.

**Cold Leg Temperatures**

The RTDs in the A and B loop cold legs were placed in the vertical part of the legs. Calculated water, gas and pipe temperature with MAAP 3.0B ("intermediate leg") are shown with an expanded temperature scale in Figures 12 and 13 for the A and B loops, respectively. The data are typically lower than any of the three temperatures in the cold legs. Again, there is a
Figure 10. Calculated gas and pipe temperatures in the A-loop hot leg compared with data.
Figure II. Calculated gas and pipe temperatures in the B-loop hot leg compared with data.
Figure 12. Calculated water and gas pipe temperatures in the A-loop cold leg compared with data.
Figure 13. Calculated water and gas pipe temperatures in the B-loop cold leg compared with data.
Temporary and apparently spurious gas temperature perturbation (in the A-loop this time, Figure 12) that begins before the core begins to uncover and that was not seen in previous (prior to Version 12) calculations using MAAP. This effect, seen in both Figures 11 and 12, as indicated above, needs further investigation. Since in both cases these are gas temperatures with inherently low energy storage potential, the overall energy balance is negligibly affected by these short-term perturbations.

For the A-loop, the predicted water temperature follows the data closely except for the noted spurious behavior between 106 and 124 minutes and beyond 150 minutes when the data indicates some cooling while the calculated water temperature tends to remain constant. Cooling is predicted at that time for the pipe. For the B-loop, the calculated water, gas and pipe temperatures remain fairly close together and deviations from data are small. Therefore the results with MAAP 3.08 for the cold leg temperatures are, for the most part, reasonably consistent with the data.

The generally colder measured temperature may indicate that cold water was continuously added directly to the cold legs from the seals of the main coolant pumps throughout Phase 2 of the accident. This coolant addition is not specified in the AE data base and therefore not modeled in this study. With the previous MAAP analysis, which assumed a continuous makeup of 1 kg/s per pump during this period, better agreement between the calculated water temperature and the data was obtained.

**Pressurizer Level**

The pressurizer level was measured by a differential pressure transducer whose taps are located at both ends of the cylindrical part of the pressurizer vessel; 33.5 feet (10.2m) apart. Therefore the maximum indicated level of 33.5 feet is measured when the volume between the two taps is full of water without any voids formed from steam or gas bubbles. If voids do exist between the taps when the water level is above the upper tap, a lower than maximum level is indicated, with the difference being directly proportional to the liquid volume fraction \((1 - \alpha)\) between the taps.
For that reason, the indicated pressurizer level between 5 and 90 minutes, when the pressurizer was full, is basically a measure of the void fraction in the pressurizer. Note also that the indicated level is referenced to the elevation of the lower tap.

The collapsed pressurizer level (referenced to the lower tap) and the calculated level response are compared to data in Figure 14. During the two-phase discharge period through the PORV, the algorithm used in MAAP to calculate the pressurizer void fraction is iterative and produces oscillatory results. Therefore, the indicated level calculated for that period is also oscillatory. However, as shown in Figure 14, this oscillatory behavior results in an average behavior that is in good agreement with the data obtained throughout most of the transient. Filling up of the pressurizer in the first few minutes is correctly calculated; and, therefore, the initial period at which steam is discharged from the PORV is properly simulated. In spite of the calculated void oscillations between 10 to 94 minutes, the maximum void fraction, which corresponds to the oscillation amplitude, is also in agreement with the data. At 94 minutes the data and the calculations indicate that the level probably receded below the PORV nozzle, leading to single-phase steam blowdown throughout the PORV. However, dryout of the A-loop steam generator and increase in system pressure results in a brief increase in the calculated level a few minutes later. After 100 minutes the calculated level shows a monotonic decrease in level while the data indicate that this decrease is delayed to 120 minutes. This is a rather minor discrepancy in the pressurizer behavior since its drainage is governed by a very small pressure drop compared to the primary system pressure, which was decreasing at that time.

The key and very challenging feature to attain for a code such as MAAP in simulating the accident progression is that the actual and calculated pressurizer drainage stopped and the level was stable and slightly rising after 153 minutes. If the pressurizer continued to drain at that time it would have caused a major core quench and probably terminated the accident. This draining of the pressurizer may have occurred at this time had it not
Figure 14. Calculated and measured pressurizer level response.
seen for the loop seal formed in the U-shaped pressurizer surge line. A small but positive pressure drop (primary system to pressurizer) is required to maintain this loop seal and prevent draining of the pressurizer.

Primary System Pressure

From the point of view of the TMI-2 accident simulation, the primary system pressure is the most important integral data comparison. This is because it is a continuing measure of the energy balance on the core, primary system and the two steam generators. As such, the pressure reflects the correctness of the boundary conditions as well as the adequacy of a code's thermal-hydraulic models.

Comparison between the calculated primary system pressure and data is shown in Figure 15. Generally, good agreement with data is obtained during most of the simulated period. Particularly good agreement is obtained from the start of the accident until 80 minutes and between 140 and 160 minutes. Relatively large deviations are noted between 83 to 94 minutes and after 166 minutes.

During the first 20 minutes the pressure response was dominated by the loss of feedwater to the two steam generators and recovery of the feedwater at 8 minutes. After 20 minutes the primary system pressure was essentially equal to the secondary side pressure. All of these transients are well simulated with MAAP. After the last B-loop pump tripped at 73 minutes the operators adjusted the auxiliary feedwater flow to both steam generators. These adjustments and their effects on the primary system pressure are not accurately simulated. This may be due to uncertainties in the AE boundary conditions and/or insufficient modeling details of the primary system mass distribution and heat transfer in the steam generators.

The good agreement with data from 100 to 160 minutes suggests that the heat transfer rate across the steam generators and the heat generated in the core during this period are adequately modeled. Of particular importance is the fact that the code is able to correctly calculate the pressurization of the primary system after the PORV valve is closed (140 to 174 minutes).
Figure 15. Comparison between calculated and measured pressure in the primary system.
During that time period, the secondary side water level in the A-loop steam generator was high enough to provide substantial secondary side heat removal potential from the primary system. However, the continuous buildup of hydrogen concentration on the primary side of the SG tubes degraded the heat transfer and resulted in a continuous increase in the system pressure. Therefore, it is critical for a code to calculate the initiation of hydrogen production early in this 140+ minute time frame.

The sharp rise in the AE data after about 170 minutes is not predicted by MAAP 3.0B. While this can reflect on some deviation from the data, it has been stated that the AE-reported pressure rise at that time (170-174 minutes) is caused by the digitized interpretation of the pressure response data during the accident and is not correct. Detailed analysis of the data from the stripchart recorder suggests that at 174 minutes the pressure had increased monotonically from 120 minutes to only 1300 psia (much closer to the MAAP value) and not the 1550 psia reported in the AE data base. The ensuing fast pressure rise should have started only after 174 minutes in response to the initiation of core quenching caused by the brief operation of the B-loop pump.

In view of this uncertainty and the additional uncertainty of plus or minus 30 to 50 psi stated in the AE data, there is very good agreement between measured and calculated primary system pressure and the pressurizer level. Both of these responses demonstrate good code agreement on the overall primary system energy balance and the balance between the heatup of the core and the effects of hydrogen accumulation in the primary system on the heat removal through the steam generators.

**Predicted Core Behavior**

In this section some key variables describing the predicted core behavior are presented. These variables cannot be compared to the very limited amount of data on instantaneous core degradation between 140 and 174 minutes in a detailed manner; but they can provide a core damage progression
that can be compared to other code predictions in an attempt to learn more about the accident and to examine the strength and weaknesses of accident analysis codes.

The predicted two-phase water level in the core is shown in Figure 16. It shows that the level temporarily dipped at 100 minutes, (when the pumps were turned off) due to the redistribution of the water inventory between the coasting down A-loop and the stagnant liquid column in the B-loop. The core then begins to uncover at 111 minutes and the level is continuously decreasing to 3 ft. (0.9 m) from the bottom of active fuel at 171 minutes. At that time MAAP calculates that some molten core material relocating downward into the water pool temporarily increased the two-phase level by about 1 ft (0.3m). Best estimates based on observation of the intact fuel rods suggests that the water level was at least about 1.5 ft (0.5m) above the bottom of the core at 174 minutes.

The calculated core hottest temperature is shown in Figure 17 along with some evidence on the core heatup progression. Note that melting of the core material was assumed at a eutectic melting point of 4040 F (2500 K) and started at 152.5 minutes. Since the plotted result always represents the peak temperature in the core region, this calculated curve should lie within or above the indicated time windows, which it does.

When a molten core region is formed, MAAP 3.0B assumes that gas flow through that particular region is terminated. After about 160 minutes, large fractions of the uncovered part of the core reached melting conditions. Hence, most of the steam generated below the water level is calculated to be diverted radially outward and flow upward through the outer regions of the core after that time. This relatively large gas flow rate calculated in the outer regions actually cooled down these regions and prevented them from rapidly overheating and melting - a condition consistent with actual TMI-2 data.
A map of the core conditions at 174 minutes is shown in Figure 18. Notations in nodes 1 to 3 at the bottom of the core represent undisturbed or original intact geometry and masses. A dense consolidated region, with essentially no porosity, is calculated between 1.04 and 1.83 m (3.4 to 6.0 ft) from the bottom of the core, with relocated density decreasing and the lower relocated boundary rising as core radius increases. The shape and location of this region is in fairly good agreement with the observed lower crust and consolidated region, as shown in Figure 18.

At the top central portion of the core, only a small fraction of core material is left by that time. Note that most of the consolidated region is formed at and above the minimum calculated water level. Some core material had relocated earlier, however, to a short distance below the minimum calculated water level. The steam and hydrogen generation due to this earlier material relocation into the water have a strong impact on the core behavior and the primary system pressure after 160 minutes. Increasing the heat transfer rate from the relocated debris to the water can result in an increase in the primary system pressure, in better agreement with the probable pressure at 174 minutes of 1300 psia.

Finally, the hydrogen production history is shown in Figure 19. At 153 minutes, when injection of auxiliary water was introduced to the B-loop steam generator, there were several tens of kg hydrogen produced in the core. Based on estimates from reference (7) this amount was more than sufficient to severely degrade the heat transfer in the steam generators and prevent cooling of the primary system. If smaller amounts of hydrogen would have been generated at that time (say 5 kg), sufficient heat could have been transferred in the steam generators to cause the primary system pressure to decrease sufficiently, and the pressurizer would have drained into the core and terminated the accident. Due to geometry degradation in the core, the calculated rate of hydrogen generation started to decrease after 160 minutes approaching an asymptotic value of 350 lb (160 kg) by 174 minutes.
Figure 18. Schematic description of the predicted core conditions at 174 minutes using MAAP 3.0B.
Figure 19. Calculated hydrogen production history.
This amount of hydrogen is consistent with the core damage that should have occurred by this time; but it is less than one-half of the total hydrogen production for the accident. Most of the remaining hydrogen to be produced was probably generated during the major core quench that began shortly after 174 minutes as a result of the short-term operation of the 2B main coolant pump at 174 minutes.

Phases 2, 3 and 4 MAAP-DOE

Steam Generator Pressures and Levels

As shown by Figures 20 through 23, levels are in good agreement with the data, while the agreement for the pressures is not quite as good. The simulated A loop SG pressure decreases to about the same pressure as the plant data show for 260 minutes, but does not do so in a monotonic fashion. Dump valve flow fraction for the A-loop SG was set to large values between 174 and 300 minutes to attempt to capture the pressure decrease. Presumably, changing the dump valve flow fraction more often during this time period would result in a better simulation. But since large amounts of hydrogen were generated at 174 minutes, the heat transfer between the primary system and the steam generators was inhibited and the primary system was not highly coupled to the secondary side.

Dump valve settings for the B-loop SG were obvious for the most part since it was isolated after 184 minutes. However, the MSIV for the B-loop was operated just after 174 minutes to reduce pressure. MAAP-DOE does not recognize an MSIV for each loop; all MSIVs are either opened or closed together. Therefore, between 176.7 and 176.9 minutes, the dump valve flow area was set to large value to mimic the operation of a B-loop MSIV. For the B-loop SG pressure, the simulated result does not have as pronounced a spike as the plant data, and the pressure after the spike is over estimated. This can be attributed to the simplicity of the MAAP steam generator model. In particular, improved modeling of heat transfer to the secondary side when the primary loop is idled would result in better agreement. But again, since the
Figure 20. Comparison of MAE prediction for B loop steam generator. Dotted lines represent plant data.
Figure 21. Comparison of MAAP-DOE prediction for A loop steam generator pressure with plant data (dashed lines).
Figure 22. Comparison of MAAP-DOE prediction for B loop steam generator level with plant data (dashed lines).
Figure 23. Comparison of MAAP prediction for A loop steam generator level with plant data (dashed lines).
primary and secondary side are not tightly coupled after 174 minutes due to the presence of hydrogen, the disagreement in the B-loop pressure should not compromise the rest of the simulation.

Hot and Cold Leg Temperatures

The A and B loop hot leg temperatures are shown in Figures 24 and 25, respectively. For Phases 3 and 4, the description of both the predicted gas and pipe temperature is very similar for the two hot legs. The B loop pump transient causes a very rapid decrease in gas temperature from over 2000 F to 1000 F. This simply reflects that after the pump transient, the gas in the two hot legs includes the steam generated by the 28 m$^3$, or so, injected to the core. Since this steam is now included in the calculation of gas temperature, and since the steam temperature is much lower than the hydrogen temperature, the gas temperature decreases very rapidly. The gas temperature stays roughly constant at 900 F until the PORV is opened at 193 minutes. The gas temperature drops quickly at that time in response to the sudden drop in primary system pressure. At 200 minutes, the introduction of cold HPI condenses steam and results in decreasing gas temperatures in the hot legs, and cold legs as well. With the PORV opening at 220 minutes, the gas temperature begins slowly declining as the amount of gas in the primary system keeps decreasing. At about 250 minutes, a large amount of hydrogen exits through the PORV, resulting in a substantial drop in both the pipe and gas temperatures in the hot leg.

As shown by Figures 24 and 25, calculated results generally continue decreasing after 175 minutes, rather than remaining fairly constant like the plant data. This is a result of the poor prediction for water level in the primary system, as will be discussed in the sections on pressurizer level and primary system pressure.

The calculated cold leg temperatures follow the same pattern as the hot leg temperatures, but the cold leg temperatures only reach a peak of about 700 F (Figures 26 and 27). As the cold leg fills up at 270 minutes, the temperatures continues to decrease. Plant data (Figure 28) shows that the
Figure 24. Comparison of HAAP-DOE predicted B loop hot leg gas and pipe temperatures with plant RTD data. (Predicted pipe temperature - solid line; predicted gas temperature - dashed line; RTD - dotted line.)
Figure 26. MAAP-DOE predicted A loop cold leg gas and pipe temperature.
(Pipe temperature - solid line; gas temperature - dashed line.)
Figure 27. MAAP-DOE predicted B loop cold leg gas and pipe temperature. (Pipe temperature - solid line; gas temperature - dashed line.)
Figure 28. Plant R10 data for the A and B loop cold legs.
calculated cold leg temperatures are overestimated after the beginning of HPI injection at 200 minutes. This is in all likelihood due to the fact that the water inventory is underestimated for the code calculations. As a result, the cold leg does not fill up until just before 270 minutes. High pressure injection covered the cold leg RTDs with subcooled water after 200 minutes and filled the cold leg entirely by 210 minutes, which kept the cold leg RTD temperature decreasing for the most part. During the core relocation of 224 minutes, however, the cold temperatures did increase for a brief time.

Pressurizer Level

Predictions of pressurizer level for MAAP-DOE are plotted against plant data in Figure 29. The pressurizer level, as predicted by MAAP-DOE, shows three difficulties: (1) the level does not rise rapidly to just over 30 feet after the pump transient, (2) the level decreases far too much after 200 minutes, and (3) the level does not increase at 210 minutes. Just after the pump transient, the code predicts an increase in water level, but does not exhibit the sharp increase shown by the plant data. This large discrepancy can be attributed to the operation of the pressurizer sprays at 175.2 minutes. At 175.2 minutes the operator opened the spray line valve connecting the A-loop cold leg to the top of the pressurizer. Although there was probably little or no spray flow through the spray line at that time since the cold leg was not filled, operation of the spray valve created a 2 inch diameter flow path between the cold leg and the pressurizer. This flow path is currently not modeled in MAAP. Hydrogen generation during Phase 2 may also have prevented a level increase by pressurizing the pressurizer gas volume. The code may have over predicted the amount of hydrogen in the pressurizer. Hydrogen generation is discussed in the next section.

Code predictions for pressurizer level show a sharp increase in water level as the PORV is opened at 192 minutes. Since gas is allowed to escape
Figure 29. Comparison of MAAP-DOE predicted pressurizer water level with plant data (dashed lines).
from the pressurizer by the PORV, the code momentarily allows a jump in water level. The water level then drops as decreasing pressurizer pressure allows flashing.

As the primary system pressure decreased following the HPI injection at 200 minutes, the pressurizer water level decreases very rapidly. Code results show an exceedingly rapid decrease in pressurizer water level. Moreover, the water level continues decreasing and does not increase until 270 minutes, in contrast to plant water level which begins increasing at 210 minutes. The code fills the primary system to the surge line nozzle at 270 minutes, rather than 210 minutes, as a result of a core steaming rate which is too large. The code continues to predict a steaming rate from the core after about 207 (when the vessel is filled to the cold leg nozzles) and MAAP-DOE assumes that condensation in the cold leg ceases when the water level in the downcomer reaches the bottom of the cold leg nozzle. This assumption may be inaccurate since the flapper valves are located above the top of the cold leg, suggesting that condensation continues until the cold leg is completely full. In addition, the code may have overstated the heat transfer rate to the water, and therefore the core steaming rate, after 207 minutes. These two effects combined to decrease the amount of water in the primary system that should have been calculated after 207 minutes.

**Primary System Pressure**

A comparison of the primary system pressure, as predicted by MAAP-DOE, with recorded plant data is presented in Figure 30. For Phases 1, 2, and 3, MAAP-DOE predictions are in general agreement with the plant data, and code predictions exhibit the same trends as the data for the most part, although the results after 207 minutes are greatly affected by the core steaming rate.

Prior to discussing the MAAP-DOE primary system pressure results for Phases 3 and 4, the MAAP-DOE Phase 2 results are considered and compared to MAAP 3.0B Phase 2 results by considering the hydrogen generation plots for the two runs. Basically, since the DOE code produces a great deal more
Figure 30. Comparison of HAAP-DOE predicted primary system pressure with plant data (dashed lines).
hydrogen during Phase 2, the DOE prediction for the primary system pressure is much more successful. The DOE code produces 900 lbs of hydrogen before the B-loop pump transient, while the 3.0B code produces 330 lbs of hydrogen before the B-loop pump transient. As a result, the DOE prediction for primary system pressure shows a rapid increase during the few minutes prior the pump transient which is not shown by the 3.0B results. In addition, the very rapid increase in pressure concomitant with the B-loop pump transient is now shown by the 3.0B results. This illustrates the limitations of 3.0B, as described in the code description appendix. Once a node is molten, in the 3.0B model, heat transfer and hydrogen generation ceases in that node, hastening core melt but underpredicting the amount of hydrogen produced.

The rapid pressure increase that occurred between 174 and 176 minutes is predicted by the code, although the peak pressures are in slight disagreement and the pressure rise is not as large as the plant data pressure rise. This disagreement may be attributed to the hydrogen production calculated by the code - Figure 31. While a total of 950 lb of hydrogen was generated, only about 50 lbs were generated after the pump transient. Since 850 to 1100 lbs are thought to have been generated, it is likely that this simulation has underestimated the hydrogen generation generated during B-loop pump transient. It should be kept in mind that hydrogen generation is greatly affected by the uncertainty in the injection between 100 and 174 minutes. A HPI flow rate of 4 kg/s was used from 100 to 174 minutes, but an only slightly different HPI flow rate history could have resulted in more hydrogen production as 174 minutes.

Depressurization at 193 minutes caused by PORV operation is slightly overpredicted by the code. Gas flow rates leaving the pressurizer may have been overestimated. The rapid pressure decrease between 200 and 204 minutes caused by HPI into the cold leg is simulated well by MAAP-DOE, but at 204 minutes, the pressure decrease ceases. MAAP-DOE assumes that condensation in the cold leg via flapper valves ceases when the downcomer water level reaches the bottom of the cold leg nozzle at 204 minutes. The predicted primary

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Figure 31. HAAP-DOE prediction for the amount of hydrogen produced as a function of time.
system pressure decreases slightly between 204 minutes and 210 minutes as the HPI flow continues and the pressurizer water drains into the vessel. At 214 minutes, the primary system pressure begins to increase in response to the predicted core steaming rate. Plant data shows that pressure increases from 1500 psia to 1700 psia in about 2 minutes due to the molten core relocation at 224 minutes. This pressure rise is not described by the code since sideways relocation is not modeled by the code.

With the PORV opening at 220 minutes, the primary system pressure begins a general downward trend until high pressure injection fills the primary system to the surge line, increases the pressurizer water level, and begins pressurizing the primary system at 270 minutes; the PORV is not closed until 318 minutes. Pressure rises slightly at 236 minutes and 257 minutes in response to HPI operation. At 236 minutes, HPI injection results in an increased steaming rate, and since the vessel is filled MAAP-DOE assumes that condensation in the cold leg cannot occur. High pressure injection is nil between 247.3 and 262 minutes. In this instance, the slight pressure increase is caused by a lack of injection. The vessel water level stays at the cold leg lower nozzle since by this time, the cold legs were partially filled. Injection at 267 minutes decreases pressure until the pressurizer begins filling at 270 minutes.

Results of one sensitivity study are presented by Figure 32. The base case revealed that the core melt drained too quickly, resulting in an overestimation of core damage. The corium viscosity was therefore increased two orders of magnitude and the simulation was rerun. The results of Figure 31 show that the increased viscosity leads to a better prediction for primary system pressure between 170 and 210 minutes.

**Core Water Level**

The MAAP-DOE core water level is shown in Figure 33; no plant data exists for comparison. Analyses indicate, however, that the vessel was filled by 207 minutes (14). Figure 33 shows that HPI and pressurizer drainage fill the vessel to the cold leg nozzles by about 204 minutes, and that the level in the core remains there for the duration.
Figure 32. Resulting primary system pressure for the corium viscosity sensitivity case. Dashed lines show plant data.
Figure 33. MAAP-DOE prediction for core water level.
Temperature and Mass Distribution

Tables 1, 2, and 3 show the core temperature distribution at 175, 224, and 300 minutes, respectively. These distributions show the temperature in each of the sixty nodes (four rings and fifteen rows) used to represent the core. The core temperature distribution at 175 minutes is in reasonable agreement with the hypothesized core configuration between 174 and 180 minutes given in Reference (8). The uppermost part of the core shows a void which appears significantly larger than the void shown in the hypothesized core configuration of Reference (8). Beneath this void, several nodes have temperatures below the melting point for ZrO₂ or UO₂ or the solidus for U-Zr-O compounds. Rows 9 through 12 each contain nodes with significant mass that have a temperature below the eutectic temperature. Rows 5 through 10 show nodes where the melting points of ZrO₂ and UO₂, or the liquidus temperature for U-Zr-O compounds, have been exceeded. Rows 1 through 4 represent the nodes which were always under the water line. These nodes have retained their original mass and their temperature is very near the water temperature in the primary system at the time.

Comparing the tabulated core temperatures to the hypothesized core configuration between 174 and 180 minutes shows that the code does a reasonable job of predicting the core state at that time. The code overstates the volume of the upper void, but it does show a volume of fragmented fuel rods overlying a volume with molten ZrO₂/UO₂. The tabulated core temperature and mass distributions also show a hemisphere like region containing molten debris.

Predictions for the core state at 224 minutes suggest that code calculations may have cooled the core too quickly. The volume of molten metal and ceramic predicted by the code is less than the volume shown by the hypothesized core configuration at 224 minutes. Nodes in rows 5 through 8 have temperatures above the ZrO₂/UO₂ melting point, but the tabulated temperature distribution does not represent a hemispherical volume of molten material occupying the center of the core. Furthermore, the molten material
### TABLE 1. TEMPERATURE DISTRIBUTION AT 175 MINUTES (°F)

<table>
<thead>
<tr>
<th>Row</th>
<th>Ring 1</th>
<th>Ring 2</th>
<th>Ring 3</th>
<th>Ring 4</th>
<th>Ring 5</th>
</tr>
</thead>
<tbody>
<tr>
<td>15</td>
<td>6.150E+02</td>
<td>6.177E+02</td>
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Core temperatures from top of core to bottom, inside to outside the outside ring represents the core barrel/baffle; the top row represents the unfueled region at the top of the fuel pin, includes the upper fission gas plena, and contains no UO₂.
TABLE 2. TEMPERATURE DISTRIBUTION AT 224 MINUTES (°F)

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Core temperatures from top of core to bottom, inside to outside the outside ring represents the core barrel/baffle; the top row represents the unfueled region at the top of the fuel pin, includes the upper fission gas plena, and contains no UO₂
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Core temperatures from top of core to bottom, inside to outside the outside ring represents the core barrel/baffle; the top row represents the unfueled region at the top of the fuel pin, includes the upper fission gas plena, and contains no UO₂.
does not extend to the outermost ring of the core; the node temperatures for ring 4 are cool. This suggests a sideways relocation could not occur, which is contrary to our knowledge of the accident. This also suggests that the core has been cooled too quickly, which coincides with the difficulty of the core has with the pressurizer level and primary system water inventory. The code overestimates steaming rate from the core, thereby cooling the debris too quickly and generating too much steam to predict the correct water level in the primary system.

Table 4 shows the mass distribution at 175 minutes. Nodes having zero mass shown the void formed by the core collapse. Rows 1 through 4 retain their original mass since they are below the minimum water level. Rows 6 through 10 have larger masses due to the melt relocated from the upper (11-15) nodes. This mass distribution changes very little during Phases 3 and 4, signifying that the core is cooled effectively during this simulation. Furthermore, temperatures in the fourth ring drop between 175 minutes and 224 minutes, and drop again between 224 minutes and 300 minutes.

### III. SIGNIFICANCE OF THE RESULTS

**3.0B Phases 1 and 2**

The first 73 minutes of the TMI-2 accident represents a straightforward application of existing PWR thermal-hydraulic models during a two-phase blowdown of the primary system. The main feature of that period is that the behavior of the steam generators controlled the primary system pressure. Spraying of auxiliary water on the SG tubes was effective in removing decay heat from the primary system even though the level indicator showed no water on the secondary side (between 8 and 20 minutes). The stuck open PORV results in a high water inventory in the pressurizer for this period of time with a void fraction of only -4%. This does not reflect the void fraction in the primary system that eventually increased to the order of 70% at the end of the first phase (-100 minutes). Hence, it is recommended in accident analysis that the pressurizer should be modeled separately and should not be lumped with any primary system node.

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By 73 minutes, both of the B-loop pumps were idled while the A-loop pumps were still operating. Calculating the correct water distribution in the primary system under these conditions requires good predictive tools for the two-phase pressure drop in the core. Some deviations from the data with MAAP 3.0B between 73 and 100 minutes may be attributed to the simple modeling of the mass distribution. This is noted especially in the comparisons with the B-loop steam generator pressure and the primary system pressure during that time. As for the flow communication with the pressurizer, MAAP models the flow regime in the A-loop hot leg as annular flow towards the end of phase 1. Therefore, it allows for only water to enter the surge line. This approach appears valid since allowing for steam carry over into the surge line under these conditions results in large deviations from the measured pressurizer level and failure to correctly simulate the second phase of the accident.

After 100 minutes, a stratified coolant configuration was established throughout the primary system with the water continuously boiling off from the core. The core started to uncover around 111 minutes and thereafter produced superheated steam. The pressure in the primary system was controlled during this period by the A-loop steam generator. Effective heat transfer due to steam condensation was probably the main heat removal mode from the primary system. As the core continued to heatup, a noticeable amount of hydrogen was generated in the core. The hydrogen was swept to the then active condensing regions in the primary system; and therefore, started to degrade the heat removal capability of the A-loop steam generator. As a result, the pressure started increasing in the primary system in spite of the decrease in the A-loop steam generator pressure and the high water level on the secondary side. All of this is modeled sufficiently well with MAAP.

The continuous buildup of hydrogen concentration in the steam generators is the key to the successful simulation of TMI-2 with MAAP. Hydrogen degraded the steam generator condensation rate and the heat removal capability, causing the end of primary system depressurization beyond the 140 minute time frame. This indicates that the energy rejection from the primary system was less than the heat generated in the core. Due to the pressure
increase. Steam and hydrogen continued to flow into the pressurizer, preventing it from draining into the core.

This continued increase in the primary system pressure represents the main sensitivity to a successful simulation. If the pressure is predicted to decrease, or even to remain constant, the pressurizer is calculated to drain into the core, quench it and terminate the accident.

The core models in MAAP 3.0B account for ballooning of the cladding, natural circulation, two dimensional (r,z) gas flow distribution in the core and a core material relocation. In addition, it assumes that gas circulation is stopped in core nodes that reach melting conditions. It is interesting to note that even with this assumption, that limits high gas temperatures at the top of a melting core, the code still overestimates the heatup of the hot legs in both loops. This result is possibly due to an overestimate in MAAP of the effectiveness of natural circulation in moving energy from the RPV to the balance of the primary system.

All of these core models do not have a significant impact on the predicted TMI-2 behavior since the main controlling phenomenon in the TMI-2 accident, according to MAAP, is the balance between the steam generators. Uncertainties in boundary conditions and plant geometry that effect the core makeup and water level (e.g., the coolant volume distribution in the cold legs and the vessel) may have stronger impact on the simulation results than the uncertainties in the core model parameters.

MAAP-DOE Phases 3 and 4

This report illustrates the importance of the model additions to MAAP-DOE for predicting plant behavior during the late phases of a core damage sequence including the effect of quenching. The two main model additions to MAAP-DOE, namely the core melt relocation and the recovery of a
edly degraded core, are key to the ability of the code to predict the plant behavior during the later phases of the TMI-2 accident.

Good agreement is obtained with the primary system pressure and with the overall hydrogen production during the accident. Sensitivity studies show that particularly good agreement for Phases 2 and 3 is demonstrated when the melt relocation is slowed down using a high viscosity for the core melt. While this high viscosity does not necessarily represent the true viscosity of the melt, it illustrates that better agreement with the data is obtained when the melt progression is slower than that predicted by a low viscosity film flow for the melt using Hoffman's data for U-Zr-O phase diagrams and solubility. This leads to the thought that perhaps a rivulet model that is controlled by surface tension is a better representation for the melt drainage.

Interesting insight to the early core damage phenomena is obtained by comparing the results of MAAP 3.0B and MAAP-DOE for the time period of 154 to 174 minutes, when melting of core material occurred above the water pool. MAAP 3.0B, by employing a non-mechanistic core blockage criteria, underestimates the system pressurization after 164 minutes (Figure 15) consistent with a simultaneous decrease in the hydrogen production rate (Figure 19). The core damage at 174 minutes, shown in Figure 18, is fairly consistent with the expected damage at 174 minutes. On the other hand, MAAP-DOE shows much faster pressurization, higher hydrogen generation and core damage. This illustrates that a viscous flow model for the melt relocation does not provide sufficient resistance to gas flow and allows for continued oxidation at a rate which is probably too high. Indeed when the relocation process is slowed down by using a high viscosity much better agreement is obtained with data.

The above observations lead to a conclusion that the blockage assumption of coolant channels based on a temperature criteria, leads to a somewhat underestimation of the oxidation rate. On the other hand, a viscous film
model for melt relocation does not provide sufficient restriction to the oxidation process. A slow relocation process, probably controlled by surface tension and affected by grid spacers, provides good agreement with the data.

Another impact of the drainage model is on the rate of hydrogen generation and the damage to the core. The film flow model in MAAP-DOE maximizes the core heatup and hydrogen generation in Phase 2 by allowing molten material to wet the entire surface area of the fuel rods. This is due to the higher temperatures calculated at low elevations and the high surface area exposed to oxidation during the relocation process. As a result, at 174 minutes the calculated core damage is higher than expected, indicating fuel melt temperatures throughout the entire upper portion of the core. This extensive core damage prevents the code from predicting the formation of the upper debris bed which contains fuel and cladding debris that was not heated to melting temperatures at 174 minutes.

Even with the higher viscosity the extent of the core damage at 174 minutes is probably overpredicted and hence the predicted hydrogen generation of about 800 lbs. at 174 minutes is probably an overestimate. The fact that the predicted pressure slope between 155 and 165 minutes is higher than the measured is another manifestation of the overestimate of the core damage during the early melting process. Hence, at 174 minutes, during the first recovery attempt, only a small amount of hot metallic Zr is available for oxidation and indeed the predicted hydrogen production during B-loop transient event is low.

On the other hand, the heat transfer from the TMI core during Phase 2 is probably overpredicted and the extent of the molten pool at 224 minutes is probably too small. This is one of the reasons that relocation to the lower plenum is not predicted at 224 minutes. With the lack of predicted material relocation to the lower plenum the significance of the simulation results of Phase 4 is small.
Two key modeling efforts are required to obtain a meaningful simulation of Phase 4:

- The thermal behavior of a molten pool in the center of the core and
- The coolability of the molten jet as it relocates to the lower plenum and as it comes to rest on the lower head.

These efforts are important in explaining the conditions leading to melt relocation to the lower plenum and the survivability of the lower vessel head. Both are important accident management considerations.

The simulation of the entire primary system behavior indicates an additional modeling deficiency that is manifested in the pressurizer response. The code predicts a total drainage of the pressurizer at 215 minutes while the data indicates that the pressurizer filled up after just a momentary drainage. As discussed previously, this is probably a result of continued calculation of the net steam generation from the core. This reduces the water inventory in the primary system and fails to fill it up in time to prevent a total drainage of the pressurizer. Up to that point, however, the model predicts well most of the trends in a pressurizer level.

The steam generators models are adequate for the most part to calculate the main trends of the secondary side response. However, the correct pressure response was not always predicted, particularly during rapid changes of condition in the primary system. This, however, has only limited effect on the simulation of the TMI-2 accident since at times that the agreement in the pressure trends is poor, the thermal coupling between the primary and secondary sides was also fairly poor.

Another comparison made in this report is with the hot and cold leg temperatures. In both cases the calculated fluid and pipe temperatures are higher than the measured data. This would have some effect in calculating
the accident progression of high pressure sequences. In these cases calculated temperatures in the hot leg or the surge line may reach high values that would suggest that a primary system failure is possible at these locations. In view of the overprediction of the hot leg temperatures shown in this report, such failures may be over conservatively calculated.

In summary, the present analysis shows good agreement with the data for the first three phases of the TMI-2 accident. The melt progression model allows the code to correctly predict the overall hydrogen production from the accident and the recovery model for a badly degraded core allows for the correct calculation of the pressure response during the two recovery periods during the accident. Model improvements, however, are needed for simulating Phase 4 of the accident involving relocation to the lower head and coolability of the molten material in the lower plenum.

IV. CONCLUSIONS

The Three Mile Island Unit 2 (TMI-2) accident caused the most serious core damage in the history of the U. S. Nuclear Industry. As such, the accident offers an unique integral source of data for enhancing the understanding of core degradation phenomena and qualifying analytical techniques used to predict the progression of severe accidents.

While uncertainty in boundary conditions make it difficult to draw unique quantitative conclusions regarding the core and the primary system behavior during a severe accident, understanding of the system trends and many other insights were gained from this analysis exercise.

Many of the codes have difficulties in simulating the late phases of the TMI-2 accidents but this analysis exercise helped defining the modeling needs, the validity of some of the assumptions and expected ranges of model parameters. Since many of the severe accident codes are intended to be used as PRA and accident management tools it is fairly important that they demonstrate at least consistency with the TMI-2 data.
The insights gained from the analysis were discussed in the previous section. Briefly, they are related to the role of hydrogen generation on the heat removal from the steam generators, the pressurizer response, the role of the downcomer flapper valves, and the impact of core geometry on oxidation and convective heat transfer to the water and gas. All of these effects are considered by MAAP-DOE. Consistency between predictions and data from TMI-2 indicates that the modeling details are generally sufficient for the first three phases. However, there are many areas where improvements are needed. These are pointed out throughout this report. The main needs are for a better mechanistic melt relocation model, formation of a molten pool, melt migration to the lower plenum and its coolability. Such models will be added to MAAP 4.0, which will be used for accident management studies. This code version is sponsored by EPRI and is currently under development at FAI. It will be available in early 1991.
V. REFERENCES


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Two versions of the MAAP code have been used to simulate the TMI-2 accident. MAAP 3.0B has been used to model Phases 1 and 2 of the accident, and the enhanced version of MAAP developed for the United States Department of Energy, MAAP-DOE, has been used to model all four phases of the accident. The DOE version of the code has improved models for core melt progression, core materials interaction, and recovery of a badly damaged core, but otherwise, it is the same as the 3.0B version of the code. Therefore, the 3.0B version of the code is described first, and then, the modeling improvements that make up the DOE version of the code and allow it to simulate all four phases, are presented.

MAAP 3.0B

The Modular Accident Analysis Program (MAAP code), Version 3.0B, has been developed and implemented as a means to understand and assess the phenomena occurring in severe accidents for IDCOR reference plants. MAAP simulates an accident transient and specifically accounts for system events that occur during the transient, including operator interventions, and continues either until a permanently coolable state is achieved or until the containment has failed and depressurized. Models are included for all of the important phenomena that might occur during accident sequences leading to degraded cores.

The MAAP code contains a structure for modeling the primary system thermodynamics, core heat-up, degradation, and melting, fission product behavior, engineering safety features, and containment thermodynamics. The hallmark of the code is that it represents a large number of interacting phenomena with simple first-principle models, rather than representing a few phenomena with very detailed models. For several reasons, these simple models, integrated together, have proven to be adequate for simulating a wide
range of accident scenarios and plant types. In particular, detailed models are not warranted for only a few phenomena if other equally important phenomena are not well understood. One-dimensional models have proved sufficient for an integrated severe accident code. Quasi-steady models are employed since errors in the rates of physical processes are generally unimportant for the time scales associated with severe accidents.

Within each of the major areas of the code, specific phenomenological models are developed to describe the degraded core and fission product behavior. It is important to note that the fission product behavior and the primary system behavior are treated within the same code package. This ensures proper feedback between the fission product and degraded core phenomena and eliminates the potential for occurrence of phenomenological behaviors which are not physically consistent.

Primary System Model Since containment integrity is the focus of the MAAP code, the primary system has been designed largely to predict the driving function for the containment response. The thermal-hydraulics models for the primary system are simplified, but the primary system model does calculate the in-vessel generation rates for hydrogen, molten core material, fission products, and the release to the containment of hydrogen, steam, and molten core material. To predict the release rate to the containment, the primary system model must predict gas and structure temperatures in the primary system, as well as the flows between the primary system and containment. As a result, the following primary side quantities are tracked:

1. primary system pressure,
2. materials temperatures,
3. void fraction of coolant at the exit points of the primary system,
4. hydrogen concentration and fission product loading of gas throughout the primary system,
5. water masses in the primary system sub-volumes, and,
6. forced and natural circulation flows within the primary system.
In order to track these quantities, the primary system model consists of 15 control volumes and 17 heat sinks. Control volumes are as follows:

- Core region, including core and core gas nodes,
- Upper core plenum,
- Reactor vessel dome,
- Broken loop hot leg, which includes the steam generator plenum,
- Broken loop hot steam generator tubes,
- Broken loop cold steam generator tubes,
- Broken loop intermediate leg, which includes the cold side steam generator plenum, loop seal and reactor coolant pump,
- Broken loop cold leg,
- Unbroken loop hot leg,
- Unbroken loop hot steam generator tubes,
- Unbroken loop cold steam generator tubes,
- Unbroken loop cold leg,
- Reactor vessel and downcomer and lower plenum,
- Steam generator gas node.

Emergency safety features which communicate directly with the primary system are modeled as mass and energy inputs, including emergency core cooling systems in injection and recirculation modes, emergency feedwater systems, refueling water storage tank, and condensate storage tank.

The pressurizer is a single region comprised of water and gas volumes, with appropriate models for flows through the surge line, relief valves and safety valves. For the surge line, flows induced by pressure differences between the primary system and the pressurizer, and drainage of water from the pressurizer to the primary system for those plants that do not have a loop seal, are considered. Depending upon the pressurizer state, flows from open relief or safety valves are either two-phase or all liquid or gas. Pressurizer heaters and sprays are accounted for as mass and energy flows to the pressurizer. The internal processes of flashing, rainout, wall heat
transfer, and decay heat generation from fission products in the water or gas volumes are considered.

Steam Generators The steam generator subroutines in MAAP calculate the thermodynamic properties and mass and energy rates of change for the water and gas volumes in the steam generator secondary side. One steam generator is in the broken loop and thus may have thermodynamic and process variables different from the steam generator in the unbroken loop. Thermodynamic properties and rates of change are calculated based upon the available steam flow area and the volume of noncondensable gases in the steam generator tubes. The available flow area is calculated at each time step based on the positions of the safety and relief valves in the broken and unbroken steam generator, and also on the main steam line isolation valve. The volume of hydrogen which may be blanketing the heat transfer area in the tubes is calculated in a separate subroutine. An equilibrium thermodynamic model is used when the tubes are covered, and a non-equilibrium model is used after the tubes become partly uncovered to allow the existence of superheat.

Core Phenomenological Models The core is modeled by a two-dimensional nodal representation with a maximum of 70 core nodes. Phenomenological core accidents are listed and described below:

1. Zircaloy oxidation and hydrogen formation,
2. Core-upper plenum natural circulation,
3. Radial radiation model,
4. Heatup of an uncovered node,
5. Melt progression model.

Zircaloy oxidation occurs at reaction rates governed by the Cathcart equation at temperatures up to 1850 K, and by the Baker-Just equation at higher temperatures. Zircaloy oxidation is terminated if the node is melting or if the node has less than a user specified non-fuel fraction. The entire process is, of course, limited by the availability of steam and zircaloy.
Natural circulation flows can be established between the upper plenum and the core, which can alter accident progression by delaying the onset of core oxidation, heating the upper plenum and revolatilizing fission products, or supplying steam to prolong the oxidation process. To avoid detailed calculations, the flow path between the upper plenum and the core is selected by the user, rather than determined mechanistically. The flow path can be one large loop coupling the core to the upper plenum, or, as is the case for Babcock and Wilcox reactors, a return flow could pass down the core barrel-baffle annulus and through the baffle into the core. A flow rate is derived assuming one of these flow patterns. In addition, inter-channel gas flow is calculated for the uncovered core region by solving a momentum equation for all radial nodes at an axial elevation. The gas flow distribution is important for evaluating the oxidation potential at high core temperatures and the convective heat transfer to the gas. Fuel rod ballooning, which affects the hydraulic resistance of the coolant channels, is therefore modeled.

In an accident involving core recovery, high temperatures may be obtained at the central core assemblies while the temperatures of the outer core assemblies may be much lower, causing a large driving potential for radial radiation heat transfer. This phenomena has been incorporated in MAAP 3.0B by an approximate radiation model which compares favorably with a more calculations.

The heatup of an uncovered node is described by an energy balance which includes the following: decay heat in a node, heat gained by oxidation, convection heat transfer to gas, radial radiation, and rate at which molten corium is leaving or entering the node.

The melt progression model in MAAP 3.0B assumes that core constituents form a eutectic at a user specified temperature with a user specified latent heat. This is a considerable simplification. As the materials melt they run downward until they reach a node which is frozen or until they reach a node.
which is already completely full. The internal energies of the molten material and still-frozen material are mixed, which usually freezes the material.

Fission Product Behavior For each of the thermal-hydraulic sections of the code, there is a corresponding fission product section to ensure proper feedback within the entire code package. The fission product rates of change require information from the corresponding thermal-hydraulic section such as volumetric flows, temperatures, etc. Once the fission product masses have been updated, the thermal-hydraulic calculations require information regarding fission product heating in order to calculate the overall energy feedback. A subroutine for each fission product region (primary system, pressurizer, containment compartments, etc.) calculates the convective transport and internal transitions between vapor, aerosol, and deposited masses for all of the twelve fission product groups which can be considered.

Fission product vapor and aerosol behavior is based upon physical laws for the phenomena of diffusiophoresis, thermophoresis, coagulation, settling, condensation and turbulent impaction. For the aerosol particle size distribution describing coagulation and deposition, correlations of numerical solutions to the particle size distribution integrodifferential equation (Smoluchowski equation) are used to keep computer run time at a minimum. Fission product chemistry is based on complete analysis of species formation possibilities and environmental conditions, and revaporization of condensed and settled aerosol materials based on physical properties of the chemical compounds formed. To describe convective transport between regions, entrainment of aerosol materials in high velocity gas streams is modeled.

Containment Model The reactor consists of four major volumes: reactor cavity - the volume below the reactor vessel, lower containment - volumes below the operating deck and inside the crane wall, annual compartment - volume outside the crane wall and below the operating deck, and, upper compartment - volume above the operating deck. Within each of these volumes, the code tracks the fluid characteristics of steam, air, hydrogen, non-condensable gases and
fission products. Within the lower compartment and the reactor cavity, corium and water are also tracked. The containment model also includes the containment spray system, containment fan coolers and hydrogen ignition sources. Figure 1 shows the containment nodalization and the flowpaths used to track materials in the containment model.

MAAP-DOE

The core melt progression model for MAAP-DOE improves upon the 3.0B model and allows simulation of Phases 3 and 4 of the TMI-2 accident. These improvements involve core material interactions, melt progression and modeling of degraded core geometry. Each of the models for these phenomena is briefly considered here and contrasted to the corresponding model for 3.0B.

In the DOE model, the fuel composition consists of uranium dioxide, zirconium dioxide, and a U-Zr-0 compound that replaces zircaloy in the 3.0B model. The U-Zr-0 compound is defined to initially consist of Zr only. Hofmann's kinetics model is used to determine the growth of the U-Zr-0 compound by uranium oxide dissolution and oxygen (to form α Zr (0)). Zircaloy oxidation and hydrogen formation remain as they were in MAAP 3.0B, namely, the reaction rates are calculated using the Cathcart or Baker-Just correlation, depending upon the cladding temperature.

Two improvements have been made for the DOE core melt progression model. First, the melt need not have the same content as its original fuel, and second, the melt travel time and the freezing destination are explicitly evaluated. This flow mechanism involves the theory of quasi-steady laminar viscous flow without considering capillarity; excluding capillarity overestimates draining distance and underestimates the potential for blockage formation. This is in contrast to the 3.0B model, which has a single eutectic temperature for uranium dioxide, zirconium dioxide, and zircaloy. Moreover, for the 3.0B model, the liquified melt has the same content as the solid material in the node and flows into the next lower node instantaneously.
Figure 1. Containment nodalization and flowpaths.
In order to calculate the heat transfer from a core region to the coolant it is necessary to determine the region (node) heat transfer area, flow area and hydraulic diameter. The latter two parameters are important for the coolant flow distribution in the uncovered section of the core. Four types of core node geometry are considered:

1. original or slightly distorted,
2. loose debris bed created by the collapse of the core,
3. partial block debris bed created by melt relocations, and,
4. highly packed, "crust like" debris bed.

These types are depicted in Figure 2. Distinctions between Types 1, 2, and 4 are made based on the volumetric porosity of the core node. For a volumetric porosity between 30% and 10% Type 3 geometry is assumed, while for a porosity of 10% or less Type 4 geometry is considered. These switchover porosities can be changed by the user for sensitivity studies.

Changing node geometry from Type 1 to 2 (collapsing a core channel) is currently determined by a logical statement that requires two simultaneous conditions:

1. the existence of an extensive molten column, defined to be at least 3 nodes that reach melting on top of each other, and,
2. high heat removal rate from the core (taken as at least five times decay heat).

When these conditions are satisfied, a loose debris bed with porosity of 0.4 is assumed to form above the location of the dense debris (Type 3) or crust (Type 4). All the core material in the collapsed core channel above the dense debris is assumed to stack up and fill up the nodes at this porosity. Hence the top node in this loose bed can be shorter if there is not enough material in that channel to fill it up.
Figure 2. Core node geometry types.
Appendix H

Gesellschaft für Reaktorsicherheit mbH

H-1
Attachment 2

Analysis of Phase 1 Using the GRS Code

ATHLET

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I  INTRODUCTION

Two calculations were carried out with ATHLET using different boundary conditions in connection with the secondary side of the OTSG's. The first calculation was carried out in 1987 without simulating the secondary side but using estimated energy transport rates from INEL from the primary to the secondary side of the OTSG. In the second calculation, carried out in 1988, the secondary loops were simulated but the measured secondary pressure and AFW injection estimated by INEL were used as boundary conditions.

All other boundary conditions were the same in both calculations. HPI/make-up flow rates were simulated as fills in the given position at each cold leg. The letdown flow recommended by INEL was simulated as a leak in the cold leg A1.

II  RESULTS

A  Node Diagram

In both calculations the two primary loops A and B of TMI-2 were completely simulated with 4 identical primary coolant pumps and two OTSGs. Fig. 1 shows the simulated pressure vessel together with one primary loop and the pressurizer. This figure is produced by a special graphics program which was developed to support the ATHLET user in examination and documentation of the geometry and nodalization of an input data set.

The objects core-bypass and down-comer are simulated as annulus but graphically represented as cylinders. Also the vent valve connecting the upper down comer with the upper plenum is simulated. The horizontal
lines in each object (e.g. CORE in fig. 1) represent the subdivision of an object in different control volumes. All important functions in the pressurizer like pressurizer-spray and heaters were simulated. The power operating relief valve (PORV) was simulated as shown in figure 2 in order to allow the application of the one-dimensional finite difference (1-D FD) exit flow model.

The ATHLET model for the TMI-Primary system is composed of 33 objects including 131 control volumes and 118 junctions. The 67 heat conduction elements are used to simulate heat generation in the core, heat transfer between core, core bypass and downcomer, heat transfer between primary and secondary sides of both OTSG's and also to simulate heat losses from pressure vessel and connected pipes to the surrounding.

The second calculation using ATHLET was carried out with simulated secondary side of the OTSG's. The initial conditions of the OTSG defined in (1) were achieved in a pretransient calculation. Steady state initialization in the used ATHLET version was applicable for U-tube steam generators (UTSG) but not for OTSG's. A modification of the steam generator model was carried out in order to include superheated steam in the steady state solution. A steady state initialization of OTSG is now available in a new version of the ATHLET code.

Both OTGS's are simulated according to the informations given in fig. 3. Feedwater injection is simulated as a fill junction, the downcomer is represented by 2 control volumes (annulus) and the volume between downcomer orifice and lower tube sheet is represented by a lump. The central part of the evaporator is subdivided into 7 control volumes while the volume above the auxiliary feedwater nozzles is represented again by a lump and the steam outlet plenum by an annular control volume. The measured secondary pressure is used as boundary condition in the time dependent volume (TDV) shown in fig. 3 which is connected by a pipe to the steam outlet of the OTSG. Several heat conductors are defined to allow heat transfer from primary to secondary loop, from evaporator to downcomer and from evaporator to steam outlet plenum. Also heat losses to the environment are taken into account. A total of 58 objects with 226 control volumes, 168 junctions and 118 heat elements simulate the whole TMI-2 system.
B Discussion of Results

The heat transfer rate from primary to secondary side during the OTSG Phase 1 of the TMI-accident was estimated by INEL and given by the dotted line in figure 4. The energy transferred due to this curve was so small that an increase in the primary pressure to more than 16 MPa at 3600 seconds was calculated as shown in figure 5. A second energy transport curve (full line in fig. 4) was approximated by engineering judgment and matching the primary pressure (Fig. 6). With this curve good agreement was achieved between measured and calculated water level in the pressurizer as shown in figure 7.

In the second calculation a pretransient calculation started at -100 s with a feed water injection rate of 800 kg/s (nominal value is 720 kg/s) and lower primary pressure, in order to achieve steady state initialization without superheated steam. During the transient phase the feed water injection rate was decreased to the nominal value while the temperature and pressure in the primary loop were allowed to increase to the defined initial conditions before the feed water pumps were stopped at -1.0 s.

The auxiliary feed water estimated by INEL (2) is injected mainly (80 %) in the position defined in fig. 3. Only 20 % are injected in the lower part of the evaporator.

The calculated primary pressure is compared with the measured value in fig. 8. A good agreement is found between calculated and measured water level in the pressurizer as shown in fig. 9. Differences between measured and calculated fluid temperature at the pump inlet (fig. 10) are comparable with the difference between calculated and measured system pressure. This is mainly due to the behaviour of the OTSG's which are still under special consideration in the GRS-analysis of the TMI-2 accident.

III SIGNIFICANCE OF THE RESULTS

Details in the simulated geometry of the plant have some times big influence on the achieved results. Special attention has to be given to the be-
behavior of the aspirator (steam bypass given in Fig. 3) in both steam generators.

The influence of the aspirator on the calculated behavior is demonstrated through the two following examples:

1. Without simulating the steam bypass in the secondary side of OTSG's a counter current flow at the orifice delays the water flow from downcomer to evaporator and the evaporator dries out at 20 s. As the upflow of steam at the orifice decreases, a sudden increase in water flow from downcomer to evaporator occurs at 100 s. The increase in heat transfer from primary to secondary loop at causes a decrease in primary fluid temperature as shown in figure 11. The water level in the pressurizer decreases then to its minimum measured value at 120 s instead 60 s (measured) as shown in fig. 12. A continuous flow of water from the downcomer (more than 10,000 kg water in each OTSG) to the evaporator has been achieved through the simulation of the aspirator and defining a mixture level in the downcomer. The behaviour shown in Fig. 12 had led us to the assumption that ATHLET with the full-range Drift-Flux model described in (3) will be able to calculate Counter Current Flow Limitation (CCFL) phenomena occurred in the surge line of the TMI-2 pressurizer at about 135 Min.

2. Geometry and flow resistance of the aspirator are not included in (1). The assumed value for the calculations shown in the figs. 8, 9 and 10 seems to be not the correct one. Calculated and measured fluid temperature at the inlet of pump 1 B during the initial 100 s are compared in fig. 13. The calculated fluid temperature is about 6 K lower than the measured value at 100 s. This difference decreases to 2 K, if the flow resistance in the aspirator was increased by an order of magnitude as shown in fig. 14.
IV CONCLUSIONS

The analysis have shown the ability of ATHLET to calculate the thermal hydraulic phenomena occurred during Phase 1 of the TMI-2 accident. Without simulating the secondary side it is possible to calculate the correct behaviour of the primary loops if correct values for the energy transport rates through each OTSG were available.

With the basic thermohydraulic models in ATHLET in combination with GCSM, the behaviour of OTSG's can be correctly calculated. With better steady state solution of the OTSG's together with the completion of the available data for the TMI-2 secondary loops more accurate calculations can be achieved. Since the available input data are not complete, additional parameter studies are needed in order to increase the accuracy of estimated data.
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Fig. 1  Geometry and Nodalization of TMI-2
(Section)
PORV
RC - RZ
F2 = 8.75 \times 10^{-4} \, \text{m}^2

BLOCK VALVE
RC - V2
F1 = 2.288 \times 10^{-3} \, \text{m}^2

10.5 \, \text{m} \text{ ELEVATION}

Fig. 2  ATHLET Simulation of PORV Exit Flow
Fig. 4  Power Transfer to the Secondary Loop in SG
Comparison of ATHLET R 20G with TMI Measurements

Legend:
- ○ ATHLET Cal.
- △ Measured Pressure

Fig. 5 Primary Pressure
Comparison of ATHLET R 10t with TMI Measurements

Fig. 6 Primary Pressure
Comparison of ATHLET R10t with TMI Measurements

Fig. 7 Water Level in the Pressurizer
Comparison of ATHLET Rg22 with TMI Measurements

LEGEND
○ ATHL Rg22
△ Measured Pressure

Fig. 8 Primary Pressure
Comparison of ATHLET Rgx22 with TMI Measurements

LEGEND
- ATHLET Rgx22
- Measured Value

Fig. 9 Water Level in the pressurizer
Comparison of ATHLET Rgx22 with TMI Measurements

Fig. 10 Fluid Temperature at the Inlet of Pump B1
Fig. 11  Fluid Temperature at the Inlet of Pump B1
Fig. 14 Fluid temperature at the inlet of Pump B1

Comparison of ATHLET Rgy30 with TMI Measurements
The computer codes used were different versions of the code system ATHLET (Analysis of the Thermal Hydraulics of LEaks and Transients).

The features are mainly those of the restructured DRUFAN-02 Code, which was satisfactorily assessed by calculating integral and separate effects tests from LOFT, LOBI, SEMISCALE, PKL, Battelle, Toshiba and HDR (1).

The new code ATHLET uses a modular network approach originally developed for ALMOD and described in (2). The plant to be simulated is represented by a network of pipe and branch modules. The configuration is defined by the users input data.

The thermohydraulic model is based on the conservation laws for vapor mass, liquid mass, mixture energy, and mixture momentum. The relative velocity between liquid and vapor is determined by a drift-flux model. Thermodynamic non-equilibrium between liquid and vapor is considered, assuming one phase to be saturated. Differentiation between the non-equilibrium and the saturated phase is carried out by temperature criteria which are described in detail in (3).

For the numerical solution the set of these equations is converted into a set of ordinary differential equations by the lumped parameter approach, i.e. with representation fluid quantities in a control volume. A steady state distribution of pressure and enthalpy in the fluid, temperature and power in the structures are calculated by direct solution of steady state equations and by iterative procedures. The ordinary differential equations for the transient are time integrated by an explicit-implicit method with automatic control of time step and order of consistency based on accuracy criteria. This integration procedure developed in the GRS was published in (4).

The flow channels and plena of the simulated system are modeled by control volumes connected by flow paths. Two control volume types are available in ATHLET. Within the ordinary control volume, homogeneous mass
and energy distribution is assumed. Within the non-homogeneous control volume, a mixture level is modeled. The combination of ordinary and non-homogeneous control volumes, provides the possibility to simulate the motion of a mixture level through a vertical component (5).

With the full-range drift flux model, recently developed in the GRS (6) the simulation of cocurrent as well as counter current flow is possible. The model comprises all flow patterns from homogeneous to separated flow occurring in vertical and in horizontal two-phase flow. It also considers counter current flow limitation in vertical and horizontal flow ducts with different geometrical configurations. The critical discharge rate at the break is determined by a discharge table, calculated with an one-dimensional finite difference (1D-FD)-model. In this model thermodynamic non-equilibrium and geometric details of the discharge flow path are considered (3).

For simulation of structures, electrical heaters and fuel rods, a heat conductor model and a point neutron kinetics model are available (9). The heat transfer package covers a wide range as single-phase and two-phase flow conditions. Correlations for critical heat flux and minimum film boiling temperature are included (7).

Major components (e.g. pressurizers, heat exchangers, condensers, ...) can be modelled in detail utilizing input data from basic thermohydraulic and heat conduction objects. In case of OTSG no steady state solution is possible with the available version. Development of a generalized iteration procedure is progressing to enable steady state solution in components including super heated steam like OTSG. Special models are available for the simulation of valves, pumps, accumulators, steam separators, single ended breaks, double ended breaks, fills and leaks, boundary conditions for pressure and enthalpy.

The general control simulation module (GCSM) is a special continuous simulation language providing a flexible description of protection, control and balance of plant systems by a user defined configuration of standardized control elements.
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Appendix I

Japan Atomic Energy Research Institute
Appendix: Participant Report

TMI-2 ANALYSIS EXERCISE WITH THALES-PM1/TMI

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CONTENTS

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II. Results
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III. Significance of the Results
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I. Introduction

This paper describes a TMI-2 Analysis Exercise with THALES (Thermal-Hydraulic Analysis of Loss-of-Coolant, Emergency Core Cooling and Severe Core Damage) - PM1/TMI code, which is a modified version of THALES-PM1 (Ref. 1) developed at JAERI for the analysis of core meltdown accidents of light water reactors. The purpose of the analysis is to verify the capability of THALES-PM1/TMI code to describe accident progression in the actual plant.

In the analysis, the initial and boundary conditions were based on the TMI-2 Standard Problem data base (Ref. 2, 3) which was used in performing the TMI-2 Analysis Exercise by OECD/NEA/CSNI. All the components in the primary coolant system were modeled in the calculation, but secondary side was modeled by one volume for each loop. Major parameters calculated include the primary system pressure, the coolant mass flow rate through PORV, the coolant levels in each component of the RCS, the fuel temperature at different radial and axial positions in the core, the hydrogen generation rate, and the heat transfer in the steam generator.

II. Results

A. Node Diagram
Figure 1 shows the system model of TMI-2 by THALES-PM1/TMI. The primary system consist of a core, downcomers, a pressurizer, hot legs and cold legs. The secondary system is modeled by one volume for each of A and B loop respectively. In this figure, the cold leg is defined as single volume including pump suction and the primary side of the steam generator.

The fuel rods are equally divided into three groups, that is a central group, an intermediate group and a peripheral group. Each fuel rod is axially divided into 24 nodes, and the peaking factor are given to each axial node. Therefore the core is divided into 72 regions, and the heat transfer between the cladding and the coolant is calculated at all of the 72 regions.

It was assumed that a node in the core becomes a debris node as the temperature of the node reaches 2800°C. Before reaching this temperature it was assumed that cladding melt took place at 1800°C. As mentioned in Appendix: Code Descriptions, THALES-PM1/TMI considers seven relocation mechanisms. In the present calculation, it was assumed that the debris node moves down to an adjacent lower node. This assumption is based on the final core configuration obtained through the core bore examination of the TMI-2 in which large debris region being supported by standing rods was formed in the middle of the core.

Concerning the interaction between the cladding and steam, it was assumed that one tenth of steam generated in the core interacts with Zr. This assumption is based on the reason that THALES-PM1/TMI does not consider the effect of steam starvation
due to the blockage in the core. If the core blockage is significant, the steam starvation effect should be considered by specifying the value of interaction rate between the steam and the cladding. Though the percentage of steam which interacts with Zr should be changed as the core geometry changes, the present version of the code does not have such capability. Thus the value of 10% was assumed as an average value for the nominal case in the present analysis considering the steam starvation due to core geometry change. The discharge coefficient for the PORV of the pressurizer was specified as 0.7.

Initial and boundary conditions were basically based on the TMI-2 Standard Problem data base (Ref. 4, 5). For the make-up/HPI and letdown flow rate, time dependent value shown in Figs. 2 and 3 was used as boundary condition which was given in the Standard Problem data base as best estimate value. For the auxiliary feed water injection rate which was not recorded during the accident, the best estimate value was used for the phases 2 to 4, but the value was modified for the phase 1. The reason for the modification is that THALES-PM1/TMI does not have the capability to simulate upper part injection of the OTSG. The modified AFW flow rate in the phase 1 shown in Figs. 4 and 5 was determined to realize the primary system pressure history during the accident and to realize the change of secondary side water level recorded during the accident.
B. Discussion of Results

Major parameters calculated include primary system pressure, coolant mass flow rate through PORV, coolant levels in each component of the RCS, fuel temperatures at different radial and axial positions in the core, hydrogen generation rate, and heat transfer in the steam generator.

Figure 6 shows the primary system pressure calculated by THALES-PM1/TMI in comparison with the actual data. The overall trend is in reasonable agreement between calculated value and actual data. In phase 4, the calculated value gradually underestimates the actual data. This underestimation might be due to lack of modeling of heat transfer from debris to the coolant.

Figure 7 shows the release rate of coolant from the PORV of the pressurizer and its integrated value. Since the PORV is located at the top of the pressurizer, the coolant release rate from the PORV largely depends on the water level in the pressurizer. Since the water level was full in the pressurizer during 0 to 100 minutes, the coolant release rate from PORV largely depended on the primary system pressure. The maximum coolant release rate was calculated by a critical flow model in which the Moody's correlation was applied. During 50 to 100 minutes the coolant release rate was almost constant, because the primary system pressure was constant and the pressurizer was full of coolant. After 100 minutes the coolant flow rate significantly decreased due to rapid decrease of the water level in the
pressurizer. The calculated total amount of water released from the PORV during the Phase 1 and the Phase 2 agrees with the best estimate value in the TMI-2 Standard Problem data base.

Figure 8 shows the water level in the core and in the pressurizer. The core uncovering occurred after 100 minutes into the accident. The water level in the core gradually decreased during the Phase 2 until three forths of the core was exposed to the steam phase at the end of the Phase 2. This minimum water level coincides with the length of the standing rods on the bottom of the core of the TMI-2 reactor. The water level in the pressurizer during Phase 2 was less than half of the full level. This was due to the suspension of the reactor coolant pumps in both of A and B loops. In the calculation, a dryout in the pressurizer occurred at about 167 minutes though such dryout did not occur in the TMI-2 accident. This discrepancy may be due to the failure in simulating the flow in the surge line of the pressurizer in the calculation.

Figure 9 shows the fuel temperature at upper, middle and lower part of the rods in the central region. The upper part of the fuel rods in the central region reached 2800°C at 155 minutes into the accident. In THALES-PM1/TMI a fuel node is identified as "debris node" after the temperature of the node reached at the specified dissolution temperature; 2800°C in this calculation. Therefore debris node in this code includes both of molten material and solid material formed by cooling of molten material. In the code, the debris node is treated as uniform material which
is composed of UO₂ and Zr. The material property of the debris node is determined in the code from the enthalpy of materials using a temperature-enthalpy curve and the weight ratio of components. The upper node in the central region became debris node after 155 minutes. The debris region was deeper in the central region than the peripheral region. As mentioned in Chapter II.A, it was assumed that debris nodes moved down to an adjacent lower node. A comparative calculation made by assuming debris nodes fell down to the bottom of pressure vessel resulted in a violent pressure increase in primary system after debris formation that was not recorded in the actual data. This was due to much larger heat transfer from debris to water in the core.

Figure 10 shows the hydrogen generation rate and the integrated hydrogen production obtained in the present analysis. In the calculation most of the hydrogen was generated between 130 and 180 minutes into the accident. The total amount of hydrogen was calculated to be 460 kg. A variety of values for total hydrogen generation during the TMI-2 accident have been obtained by different analyst. Among them the present analysis almost agreed with the value obtained by Henrie and Postma (Ref. 5).

Figure 11 shows the heat transfer rate and integrated heat transferred in the steam generator. The initial level of the heat transfer rate was $2.7 \times 10^9$ J/s. The heat transfer rate decreased rapidly at the beginning of the transient due to loss of water in the secondary side of the steam generator and stayed at very low level.
III. Significance of the Results

The AFW injection rate during phase 1 in the TMI-2 Standard Problem data base was modified in this analysis. Since the primary side of the OTSG during phase 1 was either liquid or two phase flow condition, the AFW flow rate has a large effect on the thermal-hydraulic condition in the primary system. The AFW injection rate during phases 2 to 4 was not modified. The calculated thermal-hydraulic behavior with the modified AFW injection rate was reasonable in comparison with the actual data and post accident data. Concerning the hydrogen generation, the present result gave reasonable value of total amount of hydrogen generated.

The present version of THALES-PM1/TMI has limitations in modeling the surge line of pressurizer and heat transfer from debris to the coolant. Since THALES code system has been developed to use probabilistic risk assessment, some model such as debris relocation model are not based on mechanistic model. This point is another limitation on the analysis of TMI-2 accident. However through sensitivity analyses on the TMI-2 Standard Problem, we could presume what was the reasonable model. For example, concerning the debris relocation model we selected Model 2 in which a debris node moved down to the adjacent lower node, and this model gave a reasonable thermal-hydraulic behavior.
For ongoing and future works, we are making sensitivity analyses with different make-up flow rate, because this is another large uncertainty factor, and it directly affect the thermal-hydraulic behavior in the primary system.

IV. Conclusions

The following conclusions were obtained from the present analysis.

1. The analytical results generally agrees with the actual behavior indicating that the physical models employed in the code are reasonable.
2. The feed water injection rate which was not actually recorded was modified in phase 1, and it gave a reasonable boundary condition on the analysis.
3. The total hydrogen generation obtained in the present analysis almost agreed with the value obtained by Henrie and Postma.
4. The calculated primary pressure during phase 4 underestimates the actual data due to lack of modeling of heat transfer from debris to the coolant.

V. References


5. J. L. ANDERSON, "Recommended HPI (High Pressure Injection) Rates for the TMI-2 Analysis Exercises (0 to 300 minutes)," EGG-TMI-7833(Sep. 1987).

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Fig. 1  System model of TMI-2 by THALES-PM1/TMI.
Fig. 2  Make-up/HPI flow rate for boundary condition.
Fig. 3  Letdown flow rate for boundary condition.
Fig. 4  AFW flow rate for boundary condition (A-loop).
Fig. 5  AFW flow rate for boundary condition (B-loop).
Fig. 6  Primary system pressure during TMI-2 accident calculated by THALES-PM1/TMI in comparison with the actual data.
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Fig. 9  Calculated fuel temperature at upper, middle and lower part of the rods in the central region.
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Letdown flow rate
Primary system pressure during THI-2 accident
Fuel temperature at central region
Hydrogen rate and integrated production
Heat transfer in the steam generator.
Appendix: Code Description

DESCRIPTION OF THALES-PM1/TMI CODE

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THALES-PM1/TMI is a modified version of THALES-PM1 (Ref. 1), which is a member code in THALES code system. THALES code system has been developed to describe the physical processes governing the progression of core meltdown accidents including initial blowdown, core heatup and meltdown, pressure vessel melt-through, debris/concrete interaction, and containment failure.

In the THALES code system, THALES-PM1 plays a role to calculate the thermal-hydraulic behavior in the primary coolant system, core heatup, melt-progression in the pressure vessel and melt-through of the vessel for PWRs. THALES-PM1 includes following models for important phenomena which might occur during accidents including core melting:

1. Initial blowdown of the primary coolant for large and small break LOCAs.
2. Heat transfer in the steam generator.
3. Heat generation and transport in the core.
4. Core heatup including metal-water reaction.
5. Melting and slumping of core materials.
6. Interaction of the core materials with water in the reactor vessel, and
7. Interaction of the core materials with bottom head of the reactor vessel and melt-through of the vessel.

THALES-PM1 includes a system model, a hydraulic model, and mass and heat transfer model. A system model for a typical PWR by THALES-PM1 is illustrated in Fig. 1. The primary coolant system of
a PWR is modeled by control volumes and junctions. Each control volume is divided into a gas region and a liquid region. It is assumed that the pressure in the primary system is uniform and that thermal equilibrium is maintained in each control volume. THALES-PM1 calculates a system pressure, mixture levels, gas and liquid temperatures, flow rates between control volumes.

The core heatup model in THALES-PM1 treats the one-dimensional heat transfer along fuel rod, debris and coolant. Fuel rods within the core is divided into several groups and each fuel rod is further divided into several axial segments. For the fuel segments below the mixture level, the heat transfer to coolant is calculated neglecting the temperature rise of the coolant. Above the mixture level, the heat transfer between the fuel segments and the steam is considered for each fuel rod group. The steam is distributed to each fuel rod group so as to be in proportional to the number of fuel rods in each group.

THALES-PM1 incorporates the Baker-Just model for the metal-water reaction after the initiation of core uncovery. Concerning the initiation of melt progression THALES-PM1 assumes two different failure criteria; one is fuel damage due to melting and the other is fuel fragmentation due to quench.

The fuel relocation model in THALES-PM1 is based not on mechanistic model, but on code user's selection from seven relocation patterns as shown in Fig. 2 in which the relocated position of debris nodes must be defined by input data.

For the simulation of TMI-2 accident the original THALES-PM1
has limitations in the heat transfer model and the pump model. In the heat transfer model THALES-PM1 assumes fixed value of heat transfer coefficient for the gas phase and the liquid phase in the core and in the steam generator. However in the TMI-2 accident the heat transfer mechanism was more complicated. For the pump model the original THALES-PM1 always assumes phase separation in each volume even when the reactor coolant pump is in operation. However in the TMI-2 accident it was estimated that the core was covered with two-phase mixture while the pump was in operation.

Modifications to THALES-PM1 were made in the heat transfer model and the pump model to generate THALES-PM1/TMI. In THALES-PM1/TMI these two models have following characteristics.

i) Heat transfer model

THALES-PM1/TMI employs the heat transfer model in which the heat transfer coefficient between the fuel rods and the coolant or the steam in the core or between the primary and the secondary side in the steam generator is determined from correlations used in RELAP4/MOD5 (Ref. 2) as shown in Table I.

ii) Pump model

The pump model in THALES-PM1/TMI takes the two-phase mixing effect into account except for the pressurizer. When the pump is in operation, a quasi-steady state momentum balance is assumed in each loop and loop flow rate is decided.
REFERENCES:


Table I
Heat Transfer Correlations Adopted in THALES-PM1/TMI

<table>
<thead>
<tr>
<th>STATE</th>
<th>HEAT TRANSFER CORRELATION</th>
</tr>
</thead>
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<tr>
<td>Subcooled Liquid</td>
<td>Dittus and Boelter</td>
</tr>
<tr>
<td>Forced Convection</td>
<td></td>
</tr>
<tr>
<td>Nucleate Boiling</td>
<td>Thom</td>
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<tr>
<td>Forced Convection</td>
<td>Schrock and Grossman</td>
</tr>
<tr>
<td>Vaporization</td>
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</tr>
<tr>
<td>Transition Boiling</td>
<td>McDonough, Milich and King</td>
</tr>
<tr>
<td>Stable Film Boiling</td>
<td>Groeneveld</td>
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<tr>
<td>Low Flow Film Boiling</td>
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<td>Superheated Vapor</td>
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<td>Low Pressure Flow</td>
<td>Dougall and Rohsenow</td>
</tr>
<tr>
<td>Film Boiling</td>
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</tbody>
</table>
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Fig. 1 System model for a typical PWR by THALES-PM1.
Fig. 2 Fuel relocation patterns considered in THALES-PM1.
Appendix J

Japan Institute of Nuclear Safety
FP Release and Transport Analyses for the TMI-2 Accident
Exercise Problem

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I. Introduction

The TMI-2 Sample Problem Analysis undertaken in JINS (Japan Institute of Nuclear Safety) has been focused on development of core heatup, resulted fission products (FP) release from the fuel and its transport in the primary cooling system, containment and auxiliary building, and the release into the surrounding environment. To carry out these analyses in JINS, the initial thermal-hydraulic conditions have been generated by utilizing the best estimate values of EG&G calculations (Phase 1), together with the Standard Problem Data Base.

Under this background, the JINS' source term codes, SHAPE and MACRES, have been used to calculate the FP release and its transport during Phase 2 (100-174 minutes), Phase 3 (174-200 minutes) and Phase 4 (200-300 minutes). The SHAPE code calculations yielded spatial core heatup and FP release during Phase 2 and 3, where in the latter phase the CORSOR-M model has been the main feature of the analytical base, considering the estimated molten core fraction and its temperature variation. Meanwhile, FP release into the primary cooling system during Phase 4 under the post-reflooding condition has been evaluated by the empirical function which was derived by considering leaching release from particle debris and deposited materials.

Furthermore, the MACRES calculations yielded FP transport in the
primary cooling system. Finally, the release into the containment and auxiliary building has been estimated. This report roughly describes the result of these calculations, together with brief descriptions of the two computer codes used in these analyses.
II. Results

A. Node Diagram and Basic Assumptions

(1) Core layout and source inventory

To determine an initial FP source inventory in the reactor, the core power and fuel burnup distributions are required as input data for the SHAPE code calculation, and these data have been taken from the Update Information on Standard Problem Package. Those power and burnup data in the Standard Problem Package have been given in 7 axial nodes and for every individual fuel assembly, Based on which the initial power profile for the SHAPE calculation has been described in a scattered 5 zone mapping as shown in Fig. 1. The average initial local power rate, number of fuel assemblies and fuel rods in each of these core zones are summarized in Table 1. Seven axial nodes were considered following the Standard Problem Package to describe the axial power and burnup profile in each zone, where the results are shown in Fig. 2. The geometrical data of core and fuel assembly have been taken from the TMI-2 Standard Problem Package.

(2) Basic assumptions in the calculation

In carrying out the Phase 2 calculations by using SHAPE, no deformation of the regular core lattice structure has been considered due to the code restriction, except fuel ballooning in the heatup process. Some important assumptions made in the calculations are briefly noted here. First, the axial heat conduction in the fuel rod has been neglected, considering that the fuel rod diameter is small enough and the axial heat conduction would become negligibly small compared with the heat transfer in the radial direction. Second, the metal/water reaction at internal surface after cladding rupture has been restricted to take place within 6 cm of distance from the rupture points, considering the restrictive condition of steam supply to the internal surface. The last, and the most important assumption, was that the maximum cladding temperature in the heatup calculation has been limited to 2,500 K, beyond which the cladding material would melt, relocate or slump from its
original location, thus causing the metal/water reaction to discontinue there.

B. Discussion of Results
(1) In-core FP inventory

In SHAPE calculation, the in-core FP inventory is first evaluated based on the FP inventory data file which was prepared in advance and built into the SHAPE system. This FP inventory data file has been prepared by using ORIGEN-2 and edited with respect to fuel burnup step up to 40,000 MWD/T and during initial 7 days of period. Thus, the spatial mapping of the in-core FP inventory has been obtained as the spatial integral of such core mapping. Table 2 show this result.

(2) Fuel heatup behaviors

First, Fig. 3 shows the heatup behavior of cladding center temperature in Zone-1 (the hottest core zone) at different axial nodes. Prior to the core uncovery, heat transfer at cladding surface would have been that of nucleate boiling, with sufficient fuel coolability being maintained. After core uncovery, the heat transfer turns out to be steam cooling mode, which then causes the cladding temperature quickly starts to rise. Generally, the temperature starts rising from the upper region core, particularly from Node 6, where the relative power used to be the maximum prior to the accident. Also, a sudden temperature fall is noted at Node 5 at 123 minutes, but this is due to momentary water level flushing.

Next, shown in Fig. 4 is the behavior of cladding temperature at Node 6, and for each of 5 core zones. Due to rapid increase in cladding temperature and its internal pressure, the cladding tubes would have swelled and ruptured at different time depending on the spatial heatup behaviors. The calculated result indicated that the first fuel rupture would have occurred in Zone-1 fuel at 138 minutes after the initiation of the accident. In all of the 5 zones, the cladding rupture have occurred
at Node 6 axial location. After the cladding rupture, an additional metal/water reaction quickly proceeded inside the cladding surface close to the rupture points, to cause the cladding temperature rapidly increase by the oxidation reaction heat.

In Table 3, calculations on the cladding rupture are briefly summarized. The first rupture would have occurred at 138 minutes in the Zone-1, the hottest core region, and at 152 minutes in the Zone 4, the peripheral core. In the Zone-5, the outermost core region, there would be no fuel rupture to 174 minutes, the end of Phase 2 period. It is noted here that the rupture temperature tends to become higher for the ruptures occurring at later time. This result is understood that the differential pressure across the cladding thickness might decrease at later time, because the PORV closure at 139 minutes caused further pressurization in the primary cooling system. In the actual case of the accident, it has been reported that the first fuel rupture might have occurred at 138 minutes (1,2), and the present calculation gives a reasonable agreement with this result.

Then, shown in Fig. 5 is the behavior of average oxide fuel temperature in Zone-1, given at various axial nodes. According to this result, coolability of the fuel in Zone-1 has been maintained under a normal heat transfer condition up to 110 minutes. Along with the fuel heatup, superheated steam was generated and migrated to the upper core, and as the heatup further proceeded, even a reversed heat flow i.e., from steam to the cladding, has been observed in the upper core region. After 140 minutes when the metal/water reaction started dominantly and provided a sufficient heat source to the cladding, the cladding temperature has exceeded the pellet's in some cases, and a reversed heat flow from the cladding to the pellet has also been observed.

It is noted that, in Fig. 5, the oxide fuel temperature behavior has
shown a momentary peaking character after exceeding the level somewhere around 2,300-2,400 K. This is the result of the cladding oxidation being discontinued at those points, resulting in such temperature transients by cooling effect. The possible flow passage blockage by slumping effect has been neglected in this case, and thus the cooling effect would have been a bit overestimated to have this result. The reasonable oxide temperature, therefore, would stay within the shadowed area in Fig. 16, where the upper limit line has been determined considering the flow blockage effect. With this result, the oxide fuel melting would have started in between 165-177 minutes, but no definite calculation has yet been made for the upper limit case.

For the metal/water reaction behavior, Fig. 6 shows the result calculated at Node-6 axial location, and for each of the 5 core zones. For Zones 1-4, the rapid increase of cladding temperature noted after the rupture would have been caused by the additional oxidation inside the cladding tube near the ruptured location. It is pointed out here that the metal/water reaction ceased at 35% of the cladding thickness, but it was the result of the given restrictive condition to stop the reaction for the cladding temperature beyond 2,500K.

(3) Release of radioactive materials from fuel
100 - 174 minutes (Phase 2)

For each of the nuclides I-135, X-135 and C-137, its inventory in fuel and gap, and its release from the fuel to the primary system, are summarized and shown in Figs. 7-9. For evaluating the FP release rate during the initial heatup transients, the CORSOR-M model has been employed but using 1/10 value release constants to the original. Such an application has come from the fact that the model tend to give an overestimate release rate particularly for the low burnup fuel, as examined by the SFD experiment and its analysis (3). The TMI-2 is thought to be the case of low burnup fuel.
For evaluating the release from the fuel to the primary system at a
time of cladding rupture, the Lorentz & Collins' model has been used for
1 and C₄. This considers the FP release in two distinct steps, i.e., the
bursting release at a time of cladding rupture, and the continual
diffusing release from the pellets thereafter. For the release of other
elements, the WASH-1400 gap escape fraction has been applied. In
addition, all the radioactive materials released from the fuel were
assumed to be transported into the primary cooling system.

Lastly, some of the notable events observed in this calculation are
briefly described. The stepwise increase of FP release found in Fig. 9
is resulted from the cladding rupture represented by each core zone. In
Table 4, the calculated core damage status at 174 minutes is summarized,
where the cladding temperature is considered as high as 2,500 K, above the
melting point. This condition has been assumed because a so-called
"melting pot effect" would have existed by having an oxidized layer on the
cladding surface at high temperature, which would make it possible to hold
molten material within itself for a little while.

174 - 200 minutes (Phase 3)

During the period from 174 minutes when B-loop pump operation
initiated collapsing reactor core to 200 minutes when reflooding took
place, the simple CORSOR-M model has been used to evaluate the release of
radioactive materials from fuels. Calculations by this model require fuel
temperature in the reactor, and this was estimated from the TMI-2 sampling
data. The damaged reactor core has been divided into three regions, i.e.,
the molten core materials, upper core debris, and intact fuels. The
process for determining fuel temperature is roughly the following (Fig.
10).

. The molten core region has been assumed to be at its melting point,
3078 K.
The upper core debris region has been assumed to be at the temperature below melting point such that total rare gas release would become about 70% of the estimated value. This temperature was determined by parameter survey calculations using the CURSOR-M model.

The intact fuel region has been assumed to be below 800 K, because no FP release was thought to result from this region at a time of debris formation.

200 - 300 minutes (Phase 4)

The post-reflooding FP release during the period after 200 minutes has been evaluated by an empirical formula set up for the present work, considering leaching from the debris. This empirical formula set up for the FP release rate is derived from experiment and TMI-2 observed data (4.5).

The time sequence release rates evaluated for X, and C,1 for the specified three time intervals (Phase 2-Phase 4) are shown in Fig. 11. Similarly the integrated fractions of release FP in each time interval to the total inventory in the fuels are summarized in Table 5. This result indicates that about 20% of rare gas, iodine and C, would have been released in Phase 2, and about 50% in Phase 3. The elements other than rare gases would have been continually released up to Phase 4, by leaching effect. The leaching release of Ru and Sr in Phase 4 would become larger than that of iodine and C, because of larger inventory in the fuel at this later time phase.

(4) FP transport behavior

In the MACRES code calculations for FP transport in the primary coolant system, six chemical components have been considered, i.e., K, X, C, I, C,OH, R, and S. Of the gaseous FP released from the damaged fuels, those other than rare gas elements are subject to aerosol formation to
some extent, each depending on its own chemical and physical properties. During 300 minutes of period after the accident initiation, FP transport continually decrease along with the down stream direction, since being removed from the stream to result in deposition on the uncovered component wall surfaces in the primary cooling system.

However a lot of uncertainties exist in the evaluation of such FP deposition on the structure surfaces. These are mainly due to the incompleteness in the inertial collision model for the FP moving through the flow passage of complicated geometries, and also to the difficulty to evaluate thermophoresis effect of aerosols quantitatively since not knowing the temperature gradient across the gas phase to wall surfaces.

With this background, the MACRES' base case calculations considered all its options for evaluating aerosol deposition, i.e., gravitational settling, and diffusion, diffusiophoresis, thermophoresis, and inertial depositions. On the other hand, the sensitivity study cases considered only diffusion and diffusiophoresis depositions, and difference with the base case results have been investigated. For reference, the nodding employed in the MACRES calculations is shown in Fig. 12.

The volatile iodine and C, released from the damaged fuels as vapor would have moved to the upper plenum region, where they were quickly cooled to have formed the aerosols. By assuming homogeneous nucleation in the calculations, it was known that the maximum aerosol growth rate would have been 8.5x10^-3 kg/s during the period of 140 to 183 minutes. The evaluated aerosol diameter was 2.8x10^-7 m on the geometrical mean, and on the other hand its geometrical standard deviation was 1.0.

Shown in Fig. 13 through Fig. 23 are the time sequence inventories of X, and C, in the primary cooling system as being released from the fuels, and given for the upper plenum, steam generator A, and for the pressurizer. For C, the results for the base case calculations and those for the sensitivity study calculations are both shown and compared.
This indicates that the base case results give larger deposition of aerosols on the wall surfaces, since considering all effects of deposition phenomena built in the code.

Then, shown in Table 6 are calculated FP densities in the primary coolant in comparison with the measured values. The calculated results are those in liquid phase in the core and in the A loop steam generator. The calculations give reasonable agreement with the measurements, except the case for $^{9}$K. For the big discrepancy observed in the case of $^{41}$K, there would be some errors on the side of measurements judging from the inventory balance. The results also indicate a difficulty of determining the iodine density in liquid phase, because of large uncertainties existing in the aerosol behavior and coolant mixture effect in the primary coolant system.

(5) FP release from the primary coolant system

To show the release of FP from the primary coolant system, the calculated FP inventories in the drain tank and make up tank at 300 minutes and 98 hours are shown in Table 7. Since the base case calculations, compared with the sensitivity study case, give larger depositions of C,$^{1}$ I, C,$^{2}$OH, Ru, and Sr on the component wall surfaces, the release in the down stream would become smaller than the sensitivity case up to the time of 15 hours 50 minutes. After this time, however, when the primary cooling system has been refilled with water, the release of FP in the down stream would increase by leaching from the deposit, and the base case results tend to overcome the sensitivity case's thereafter. This result indicates that, if all the depositing phenomena are considered, the release of radioactive materials from the primary cooling system would continue longer period, but with smaller release rate during the earlier time phase.

Of the radioactive materials released from the primary cooling system
and transported into the drain tank, the rare gas elements (K. and X.) were then released into the containment atmosphere and detected by the dome monitors. This radiation level behavior has been calculated, with its result shown in Fig. 24 in comparison with the dome monitor measurements (6). The radiation level, both calculated and monitored, indicated a quick rise at about 192 minutes with the PORV opening, and soon exceeded the level of $10^3$ (mR/hr) at which the monitor being over scaled.

During the period prior to 192 minutes at which the PORV is opened, a big discrepancy is observed between the calculated and monitored results, where the difference is in the order of three decades at the maximum giving larger values for the monitor side. The reason for having such a big difference is probably due to the leakage from the PORV (or from the other parts of the PCS) in the actual accident sequence, which has been neglected by the MACRES calculations. On the other hand, the calculated radiation level in the containment during 139-192 minutes accounted only those K. and X. which have been released at the earlier stage prior to 139 minutes.
III. Significance of the Results

The TMI-2 Sample Problem (Phase 2-4) has been taken up and analyzed by using the JINS' source term codes, SHAPE and MACRES, the former being for spatial core heatup and FP release from fuels, and the latter for FP transport in the primary cooling system (PCS). The emphasis has been on such physical or chemical phenomena as core heatup behavior, fuel failure, resulted FP release from the fuel, FP transport in the PCS, and finally the release from the PCS to the outer plant system.

The calculations showed that the first cladding rupture would have occurred at 138 minutes after the accident initiation, and the cladding started melting sometime around 145 minutes from the hottest region, then followed by the surrounded regions. At 174 minutes, the end of Phase 2, about 20% of the in-core fuel cladding would have oxidized as the minimum estimate from the considered restrictive assumption, while the total hydrogen generation during this period would have been 165 kg along with the same assumption. On the other hand, the oxide fuel melting would have started sometime between 165-177 minutes depending on the assumption on the flow blockage condition. It is quite difficult to indicate that at what exact time the oxide melting would have actually started, because of the uncertain flow condition after relocation of the cladding material.

The released fraction of the rare gases (Kr and Xe) and volatile iodine and Cs would be each 71% to their total core inventories, while the values for the non-volatile R, and Sr, would be 3.3% and 6.1%, respectively. From the TMI-2 core bore examination data, it was shown that a comparative amount of iodine had been retained in the debris particles, and therefore, the iodine release behavior would have been somewhat different from the other FP's described above, in the core disintegration and melting process.
The released FP materials other than rare gas would form aerosols in the upper plenum region by homogeneous nucleation process. The geometrical mean diameter of those aerosol particles was calculated to be 2.8x10^-7 m, with its geometrical standard deviation 1.0. However, these aerosols would not have been released to the external environments, since being removed by scrubbing or dissolution into the liquid phase.

To evaluate FP transport behaviors in the PCS, it is important to consider their deposition on the wall surfaces of the system components, particularly prior to the core reflooding. But the leaching release from the wall surfaces might also exist following the reflooding, and it would influence the later FP transport behaviors to a great deal. Thus, the wall deposition of FP in the gas phase would not necessarily straightforwardly decrease the FP release from the PCS in the later time phase. Also, the calculations successfully reproduced the radiation level given by the dome monitor in the containment at about 220 minutes after the accident initiation.
IV. Conclusion

The TMI-2 Sample Problem Analysis in JINS has been focused on calculations of fuel heatup and FP release during Phases 2-4, and JINS' source term codes, SHAPE and MACRES, have been used for this analysis; where SHAPE is for calculations of fuel heatup and resulted FP release from the fuel to primary coolant during Phase 2 and 3 periods, and MACRES for calculations of FP transport in the primary coolant system during all phases. The calculations came out reasonable in many cases as compared with the measured data where available, but a couple of aspects have to be pointed out as the remaining problems.

The SHAPE calculation indicated that fuel heatup started at about 110 minutes and initial fuel cladding rupture occurred at 138 minutes, and thereafter, the clad temperature showed much quicker increase helped by additional zircaloy oxidation heat, easily reaching to or exceeding the clad melting temperature. Under this condition, the clad oxidation calculation was halted considering that fuel heatup could no longer continue with the original lattice geometry, due to relocation of cladding materials. Thus, only the decay heat has been taken into account in the later heatup calculation. Such an assumption as to give a sudden halt in oxidation reaction eventually caused a momentary peak in the fuel temperature behavior during this period. The more appropriate manner would be that the gradual transition be considered when halting the oxidation reaction in such situations.

Relating to the above discussion, a point to mention is the amount of zircaloy oxidation. Due to the relocation of cladding material being assumed in the SHAPE calculation, the amount of zircaloy oxidation has been limited to about maximum 35 % in its original location (about 20 % in the core average), and this result would be somewhat different to the calculations made by the other groups of the TMI-2 Task, which gave 100%
oxidation in some nodes of the core. Such differences in zircaloy oxidation eventually caused a smaller hydrogen generation in the SHAPE calculation, i.e., 165 kg (SHAPE) compared to 300-400 kg in the other calculations.

Another point to discuss is on the problem of core melt. The SHAPE calculation concluded that there would be no core melt (although very close) during the Phase 2 period, referring to "core melt" to indicate the oxide melt condition. With clad melting within a scope of core melt, there was, of course, an earlier core melt during the Phase 2 period, i.e. starting at around 150 minutes. In the meantime, one thing to mention here is the existence of channel blockage due to the clad melt and relocation. This effect has been neglected in the SHAPE heatup calculation, and therefore, the steam cooling would be a bit overestimated after channel blockage, if existing actually. More detailed studies in those aspects would be needed in the future.

As to phase 3-4 calculations for FP release into the primary coolant system from molten pool and rubble bed, it was shown that calculated I and C retention fractions in debris were both lower than the measurements. This result seems to indicate that I and C would in the actual accident case exist by forming chemical compounds with other core materials. Hence, it is important to take such retention effect into account for I and C release behaviors in the degraded core materials.
V. References

(1) "Investigation into the March 28, 1979 Three Mile Island Accident by Office of Inspection and Enforcement", NUREG-0600, VOL.1, Aug. 1979.


Table 1 Average Power Ratio in Radial Direction, Number of Fuel Assemblies and Number of Fuel Rods for Each Core Region

<table>
<thead>
<tr>
<th>zones of core</th>
<th>power factor</th>
<th>number of fuel assemblies</th>
<th>number of fuel rods</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1.38</td>
<td>9</td>
<td>1872</td>
</tr>
<tr>
<td>2</td>
<td>1.17</td>
<td>52</td>
<td>10816</td>
</tr>
<tr>
<td>3</td>
<td>1.07</td>
<td>44</td>
<td>9152</td>
</tr>
<tr>
<td>4</td>
<td>0.88</td>
<td>52</td>
<td>10816</td>
</tr>
<tr>
<td>5</td>
<td>0.58</td>
<td>20</td>
<td>4160</td>
</tr>
<tr>
<td>Isotope</td>
<td>Half-Life</td>
<td>Inventory</td>
<td>Mass (g)</td>
</tr>
<tr>
<td>---------</td>
<td>-----------</td>
<td>-----------</td>
<td>----------</td>
</tr>
<tr>
<td>Kr-85</td>
<td>10.7 y</td>
<td>$9.9 \times 10^4$</td>
<td>$2.6 \times 10^4$</td>
</tr>
<tr>
<td>Sr-90</td>
<td>29 y</td>
<td>$7.6 \times 10^3$</td>
<td>$5.6 \times 10^3$</td>
</tr>
<tr>
<td>Ru-106</td>
<td>368 d</td>
<td>$3.6 \times 10^4$</td>
<td>$1.1 \times 10^3$</td>
</tr>
<tr>
<td>Sb-135</td>
<td>2.7 y</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>I-129</td>
<td>$1.6 \times 10^3$ y</td>
<td>$2.2 \times 10^{-1}$</td>
<td>$1.2 \times 10^3$</td>
</tr>
<tr>
<td>I-131</td>
<td>8.04 d</td>
<td>$6.8 \times 10^7$</td>
<td>$5.5 \times 10^7$</td>
</tr>
<tr>
<td>Xe-133</td>
<td>5.3 d</td>
<td>$1.4 \times 10^8$</td>
<td>$7.7 \times 10^5$</td>
</tr>
<tr>
<td>Cs-134</td>
<td>2.1 y</td>
<td>$2.1 \times 10^5$</td>
<td>$1.6 \times 10^5$</td>
</tr>
<tr>
<td>Cs-137</td>
<td>30 y</td>
<td>$8.7 \times 10^3$</td>
<td>$1.0 \times 10^4$</td>
</tr>
<tr>
<td>Cs-144</td>
<td>284 d</td>
<td>$2.4 \times 10^7$</td>
<td>$7.6 \times 10^5$</td>
</tr>
</tbody>
</table>

**TOTAL** = $2.7 \times 10^5$g

*a. As calculated by ORIGEN-2*
Table 3 Cladding Rupture by the SHAPE Analysis

<table>
<thead>
<tr>
<th>core zone</th>
<th>rupture node</th>
<th>rupture time (min)</th>
<th>rupture temperature (K)</th>
<th>pressure difference between cladding inside and outside (Pa)</th>
<th>hoop stress (Pa)</th>
<th>temperature increaserate (K/S)</th>
<th>peak clad temp. (K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>6</td>
<td>138</td>
<td>1164</td>
<td>$2.41 \times 10^6$</td>
<td>$1.96 \times 10^7$</td>
<td>0.37</td>
<td>2500</td>
</tr>
<tr>
<td>2</td>
<td>6</td>
<td>142</td>
<td>1167</td>
<td>$2.35 \times 10^6$</td>
<td>$1.91 \times 10^7$</td>
<td>0.32</td>
<td>2500</td>
</tr>
<tr>
<td>3</td>
<td>6</td>
<td>144</td>
<td>1169</td>
<td>$2.29 \times 10^6$</td>
<td>$1.86 \times 10^7$</td>
<td>0.31</td>
<td>2500</td>
</tr>
<tr>
<td>4</td>
<td>6</td>
<td>152</td>
<td>1194</td>
<td>$1.76 \times 10^6$</td>
<td>$1.43 \times 10^7$</td>
<td>0.35</td>
<td>2500</td>
</tr>
<tr>
<td>5</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>1599</td>
</tr>
</tbody>
</table>
Table 4  Core Damage State at 174 min.
After Shutdown

<p>| | |</p>
<table>
<thead>
<tr>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Zircaloy beyond 2131K (%)</td>
<td>56</td>
</tr>
<tr>
<td>UO$_2$ dissolved (%)</td>
<td>0</td>
</tr>
<tr>
<td>Cladding oxidized (%)</td>
<td>19.7</td>
</tr>
<tr>
<td>H$_2$ mass generated (kg)</td>
<td>164.6</td>
</tr>
</tbody>
</table>
Table 5  FP Release Fraction from Fuel into the PCS

<table>
<thead>
<tr>
<th></th>
<th>FP released fraction into the PCS (%)</th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0~174 min.</td>
<td>174~200 min.</td>
<td>200~300 min.</td>
</tr>
<tr>
<td>Kr</td>
<td>20.6</td>
<td>36.3 + 14.8 *</td>
<td>0.0</td>
</tr>
<tr>
<td>Xe</td>
<td>20.8</td>
<td>36.5 + 14.7 *</td>
<td>0.0</td>
</tr>
<tr>
<td>I</td>
<td>20.4</td>
<td>36.0 + 14.6 *</td>
<td>0.9</td>
</tr>
<tr>
<td>Cs</td>
<td>20.5</td>
<td>36.4 + 15.0 *</td>
<td>0.9</td>
</tr>
<tr>
<td>Ru</td>
<td>8.4×10⁻⁷</td>
<td>0.1 + 0.02 *</td>
<td>3.1</td>
</tr>
<tr>
<td>Sr</td>
<td>9.7×10⁻⁴</td>
<td>2.90 + 0.23 *</td>
<td>3.0</td>
</tr>
</tbody>
</table>

* (from molten debris) + (from upper debris)

3078 K  2450 K
Table 6 (1/2) Comparison between Measured and Calculated Fission Products Concentration in Reactor Coolant. (Base case)

<table>
<thead>
<tr>
<th>Nuclide</th>
<th>Measured</th>
<th>Calculated</th>
<th>Measured</th>
<th>Calculated</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Time 165.4 min.</td>
<td></td>
<td>Time 299.4 min.</td>
</tr>
<tr>
<td>Kr-85</td>
<td>$1.437 \times 10^3$</td>
<td>$1.07 \times 10^8 \sim 1.10 \times 10^8$</td>
<td>$2.686 \times 10^9$</td>
<td>$9.88 \times 10^8 \sim 1.31 \times 10^9$</td>
</tr>
<tr>
<td>Xe-133</td>
<td>$5.007 \times 10^3$</td>
<td>$2.01 \times 10^8 \sim 5.23 \times 10^8$</td>
<td>$4.493 \times 10^9$</td>
<td>$1.77 \times 10^9 \sim 1.65 \times 10^9$</td>
</tr>
<tr>
<td>Xe-135</td>
<td>$2.60 \times 10^3$</td>
<td>$5.23 \times 10^8 \sim 1.36 \times 10^9$</td>
<td>$2.466 \times 10^9$</td>
<td>$5.80 \times 10^9 \sim 5.40 \times 10^9$</td>
</tr>
<tr>
<td>I-131</td>
<td>$1.731 \times 10^4$</td>
<td>$8.20 \times 10^9 \sim 6.98 \times 10^9$</td>
<td>$8.149 \times 10^9$</td>
<td>$1.25 \times 10^10 \sim 5.11 \times 10^9$</td>
</tr>
<tr>
<td>I-133</td>
<td>$3.02 \times 10^4$</td>
<td>$1.70 \times 10^9 \sim 1.45 \times 10^9$</td>
<td>$1.438 \times 10^9$</td>
<td>$2.43 \times 10^9 \sim 9.94 \times 10^9$</td>
</tr>
<tr>
<td>Cs-137</td>
<td>$2.944 \times 10^4$</td>
<td>$1.06 \times 10^9 \sim 2.08 \times 10^9$</td>
<td>——</td>
<td>$1.43 \times 10^9 \sim 8.27 \times 10^9$</td>
</tr>
</tbody>
</table>

(Concentration in SG-A) ~ (Concentration in core)

* EGG-TMI-7392 Sept.1986
* SOE page 60,61,106
Table 6 (2/2) Comparison between Measured and Calculated Fission Products Concentration in Reactor Coolant.
(Sensitivity case)

<table>
<thead>
<tr>
<th>Nuclide</th>
<th>Time 165.4 min.</th>
<th>Time 299.4 min.</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Measured *</td>
<td>Calculated</td>
</tr>
<tr>
<td>Krypton-85</td>
<td>$1.437 \times 10^7$</td>
<td>$1.07 \times 10^8 - 1.10 \times 10^9$</td>
</tr>
<tr>
<td>Xenon-133</td>
<td>$5.807 \times 10^7$</td>
<td>$2.01 \times 10^6 - 5.23 \times 10^7$</td>
</tr>
<tr>
<td>Xenon-135</td>
<td>$2.260 \times 10^8$</td>
<td>$5.23 \times 10^7 - 1.36 \times 10^8$</td>
</tr>
<tr>
<td>Iodine-131</td>
<td>$1.731 \times 10^8$</td>
<td>$2.94 \times 10^7 - 6.14 \times 10^8$</td>
</tr>
<tr>
<td>Iodine-133</td>
<td>$3.302 \times 10^9$</td>
<td>$6.10 \times 10^8 - 1.28 \times 10^9$</td>
</tr>
<tr>
<td>Cesium-137</td>
<td>$2.944 \times 10^9$</td>
<td>$3.55 \times 10^9 - 1.82 \times 10^{10}$</td>
</tr>
</tbody>
</table>

(Concentration in SG-A) ~ (Concentration in core)

*EGG-TMI-7382 Sept. 1986

SOE page 60,61,106
<table>
<thead>
<tr>
<th>Nucleus</th>
<th>Time (hr)</th>
<th>Base Case</th>
<th>Sensitivity Case</th>
<th>Unit: kg</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>300 min</td>
<td>98 hr</td>
<td>300 min</td>
</tr>
<tr>
<td>Kr</td>
<td>Drain Tank</td>
<td>7.50 x 10^7 *</td>
<td>2.71 x 10^8</td>
<td>7.50 x 10^7</td>
</tr>
<tr>
<td></td>
<td>Makeup Tank</td>
<td>5.32 x 10^7</td>
<td>2.05 x 10^8</td>
<td>5.32 x 10^7</td>
</tr>
<tr>
<td>Xe</td>
<td>Drain Tank</td>
<td>8.06 x 10^9</td>
<td>3.74 x 10^9</td>
<td>8.06 x 10^9</td>
</tr>
<tr>
<td></td>
<td>Makeup Tank</td>
<td>5.13 x 10^9</td>
<td>2.51 x 10^9</td>
<td>5.13 x 10^9</td>
</tr>
<tr>
<td>Cs</td>
<td>Drain Tank</td>
<td>2.60 x 10^10</td>
<td>3.64 x 10^10</td>
<td>2.60 x 10^10</td>
</tr>
<tr>
<td></td>
<td>Makeup Tank</td>
<td>5.05 x 10^10</td>
<td>6.60 x 10^10</td>
<td>5.05 x 10^10</td>
</tr>
<tr>
<td>CsOH</td>
<td>Drain Tank</td>
<td>1.08 x 10^11</td>
<td>1.68 x 10^11</td>
<td>1.08 x 10^11</td>
</tr>
<tr>
<td></td>
<td>Makeup Tank</td>
<td>4.72 x 10^11</td>
<td>7.43 x 10^11</td>
<td>4.72 x 10^11</td>
</tr>
<tr>
<td>Ru</td>
<td>Drain Tank</td>
<td>6.39 x 10^11</td>
<td>9.38 x 10^11</td>
<td>6.39 x 10^11</td>
</tr>
<tr>
<td></td>
<td>Makeup Tank</td>
<td>4.80 x 10^11</td>
<td>6.62 x 10^11</td>
<td>4.80 x 10^11</td>
</tr>
<tr>
<td>Sr</td>
<td>Drain Tank</td>
<td>2.70 x 10^11</td>
<td>4.68 x 10^11</td>
<td>2.70 x 10^11</td>
</tr>
<tr>
<td></td>
<td>Makeup Tank</td>
<td>5.31 x 10^11</td>
<td>8.62 x 10^11</td>
<td>5.31 x 10^11</td>
</tr>
</tbody>
</table>

*without Inertial deposition & thermophoresis*
Fig.1 Grouping of TMI-2 core area.
Fig. 2 Power distribution in the axial direction.
Fig. 3 Clad temperature vs. time.
Fig. 7 1-131 partitioning between fuel, gap and RCS v.s. time.
Fig. 8 XE-135 partitioning between fuel, gap and RCS v.s. time.
Fig. 9  Cs–137 partitioning between fuel, gap and RCS v.s. time.
Fig. 10 Noding and grouping of TMI-2 core.
1: FP release during fuel heatup
2: FP release from heated debris

Fig. 11 (1/2) Xe release rate into the PCS
1: FP release during fuel heatup
2: FP release from heated debris
3: FP release by leaching

Fig. 11 (2/2) CsI release rate into the PCS.
Fig. 12 Nodalization for the MACRES code
Fig.14 CsI inventory in core region coolant mixture.
Sensitivity case  Without thermophoretic adherence and inertial deposition

![Graph showing Csl inventory in upper plenum](image)

Fig. 17  Csl Inventory in upper plenum.
Fig. 18 Xe inventory in A-steam generator.
Fig. 19 CsI inventory in A–steam generator.
Sensitivity case: Without thermophoretic adherence and inertial deposition

Fig. 20 CsI inventory in A-steam generator
Fig. 21 Xe inventory in pressurizer
Fig. 24 Comparison between the containment dome radiation monitor response and calculation.
Fig. 24 Comparison between the containment dome radiation monitor response and calculation.
Sensitivity case: Without thermophoretic adherence and inertial deposition

Fig. 20 CsI inventory in A—steam generator
Base case

Fig. 19 CsI inventory in A–steam generator.
Fig. 18 Xe inventory in A–steam generator.
Fig. 17 CsI inventory in upper plenum.

Sensitivity case: Without thermophoretic adherence and inertial deposition.
Base case

Fig. 16  Csl inventory in upper plenum.
Fig. 14  CsI inventory in core region coolant mixture.
Fig. 13 Xe inventory in core region coolant mixture
Fig. 12 Nodalization for the MACRES code
1 FP release during fuel heatup
2 : FP release from heated debris
3 : FP release by leaching

Fig. 11 (2/2) CsI release rate into the PCS.
1: FP release during fuel heatup
2: FP release from heated debris

Fig. 11(1/2) Xe release rate into the PCS
Fig.10 Noding and grouping of TMI-2 core.
Fig. 9  Cs–137 partitioning between fuel, gap and RCS v.s. time.
Fig. 7 1-131 partitioning between fuel, gap and RCS v.s. time.
Fig. 6 Clad–water reaction v.s. time.
Fig. 5 Fuel pellet average temperature vs. time.
Fig. 4 Clad temperature v.s. time.
Fig. 3 Clad temperature vs. time.

- No more ZrH reaction calculations beyond the level indicated.
- Axial nodes: 0, 1, 2, 3, 4, 5, 6.
Fig. 2: Power distribution in the axial direction.
Fig. 1 Grouping of TML-2 core area.
### Table 7  Radioactive Materials Release from Primary System to Downstream Compartment

<table>
<thead>
<tr>
<th>Unit</th>
<th>base case</th>
<th>sensitivity case *</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>300 min</td>
<td>96 hr</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>2.99</td>
</tr>
<tr>
<td>Drain Tank</td>
<td>7.50 x 10^4</td>
<td>2.71 x 10^5</td>
</tr>
<tr>
<td>Makeup Tank</td>
<td>1.11 x 10^4</td>
<td>3.52 x 10^4</td>
</tr>
<tr>
<td>Drain Tank</td>
<td>3.13 x 10^4</td>
<td>1.02 x 10^5</td>
</tr>
<tr>
<td>Makeup Tank</td>
<td>2.40 x 10^4</td>
<td>3.97 x 10^5</td>
</tr>
<tr>
<td>Drain Tank</td>
<td>2.59 x 10^4</td>
<td>3.12 x 10^5</td>
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<tr>
<td>Makeup Tank</td>
<td>1.00 x 10^4</td>
<td>6.00 x 10^4</td>
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<tr>
<td>Drain Tank</td>
<td>4.55 x 10^4</td>
<td>7.81 x 10^5</td>
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<tr>
<td>Makeup Tank</td>
<td>6.29 x 10^4</td>
<td>2.31 x 10^6</td>
</tr>
<tr>
<td>Drain Tank</td>
<td>7.07 x 10^5</td>
<td>2.48 x 10^7</td>
</tr>
<tr>
<td>Makeup Tank</td>
<td>5.31 x 10^5</td>
<td>5.75 x 10^6</td>
</tr>
<tr>
<td></td>
<td>4.70 x 10^5</td>
<td>2.51 x 10^7</td>
</tr>
<tr>
<td></td>
<td>5.75 x 10^5</td>
<td>(9.09 x 10^5)</td>
</tr>
</tbody>
</table>

*without inertial deposition & thermophoresis*
Table 6 (2/2) Comparison between Measured and Calculated Fission Products Concentration in Reactor Coolant.
(Sensitivity case)

<table>
<thead>
<tr>
<th>Nuclide</th>
<th>Measured</th>
<th>Calculated</th>
<th>Measured</th>
<th>Calculated</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Time 165.4 min.</td>
<td>Time 299.4 min.</td>
</tr>
<tr>
<td>Kr-8</td>
<td>1.437x10^4</td>
<td>1.07x10^4 – 1.10x10^4</td>
<td>2.88x10^4</td>
<td>9.83x10^4 – 1.31x10^4</td>
</tr>
<tr>
<td>Xe-133</td>
<td>5.807x10^4</td>
<td>2.01x10^4 – 5.23x10^4</td>
<td>4.49x10^4</td>
<td>1.77x10^4 – 1.65x10^4</td>
</tr>
<tr>
<td>Xe-135</td>
<td>2.260x10^4</td>
<td>5.23x10^4 – 1.36x10^4</td>
<td>2.46x10^4</td>
<td>5.80x10^4 – 5.40x10^4</td>
</tr>
<tr>
<td>I-131</td>
<td>1.731x10^4</td>
<td>2.94x10^4 – 6.14x10^4</td>
<td>8.14x10^4</td>
<td>8.66x10^4 – 1.54x10^4</td>
</tr>
<tr>
<td>I-133</td>
<td>3.302x10^4</td>
<td>6.10x10^4 – 1.28x10^4</td>
<td>1.43x10^4</td>
<td>1.68x10^4 – 2.99x10^4</td>
</tr>
<tr>
<td>Cs-137</td>
<td>2.944x10^4</td>
<td>3.55x10^4 – 1.82x10^4</td>
<td>—</td>
<td>1.03x10^4 – 2.10x10^4</td>
</tr>
</tbody>
</table>

(Concentration in SG-A) ~ (Concentration in core)

* EGG-TMI-7382 Sept.1986
SOE page 60,61,106
Table 6 (1/2) Comparison between Measured and Calculated Fission Products Concentration in Reactor Coolant. (Base case)

<table>
<thead>
<tr>
<th>Nuclide</th>
<th>Time 165.4 min.</th>
<th>Time 299.4 min.</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Measured *</td>
<td>Calculated</td>
</tr>
<tr>
<td>Kr- 85</td>
<td>1.437×10^4</td>
<td>1.07×10^4 ~ 1.10×10^4</td>
</tr>
<tr>
<td>Xe-133</td>
<td>5.807×10^4</td>
<td>2.01×10^4 ~ 5.23×10^4</td>
</tr>
<tr>
<td>Xe-135</td>
<td>2.260×10^4</td>
<td>5.23×10^4 ~ 1.36×10^4</td>
</tr>
<tr>
<td>I-131</td>
<td>1.731×10^4</td>
<td>8.20×10^4 ~ 6.98×10^4</td>
</tr>
<tr>
<td>I-133</td>
<td>3.302×10^4</td>
<td>1.70×10^4 ~ 1.45×10^4</td>
</tr>
<tr>
<td>Cs-137</td>
<td>2.944×10^4</td>
<td>1.05×10^4 ~ 2.08×10^4</td>
</tr>
</tbody>
</table>

(Concentration in SG-A) ~ (Concentration in core)

*EGG-TMI-7382 Sept.1986
SOE page 60,61,106
<table>
<thead>
<tr>
<th></th>
<th>FP released fraction into the PCS (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0~174 min.</td>
</tr>
<tr>
<td><strong>Kr</strong></td>
<td>20.6</td>
</tr>
<tr>
<td><strong>Xe</strong></td>
<td>20.8</td>
</tr>
<tr>
<td><strong>I</strong></td>
<td>20.4</td>
</tr>
<tr>
<td><strong>Cs</strong></td>
<td>20.5</td>
</tr>
<tr>
<td><strong>Ru</strong></td>
<td>8.4×10⁻³</td>
</tr>
<tr>
<td><strong>Sr</strong></td>
<td>9.7×10⁻⁴</td>
</tr>
</tbody>
</table>

* (from molten debris) + (from upper debris)

3078 K

2450 K
Table 4  Core Damage State at 174 min.
After Shutdown

<p>| | |</p>
<table>
<thead>
<tr>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Zircaloy beyond 2131K</td>
<td>56</td>
</tr>
<tr>
<td>(%)</td>
<td></td>
</tr>
<tr>
<td>UO₂ dissolved</td>
<td>0</td>
</tr>
<tr>
<td>(%)</td>
<td></td>
</tr>
<tr>
<td>Cladding oxidized</td>
<td>19.7</td>
</tr>
<tr>
<td>(%)</td>
<td></td>
</tr>
<tr>
<td>H₂ mass generated</td>
<td>164.6</td>
</tr>
<tr>
<td>(kg)</td>
<td></td>
</tr>
<tr>
<td>core zone</td>
<td>rupture node</td>
</tr>
<tr>
<td>-----------</td>
<td>-------------</td>
</tr>
<tr>
<td>1</td>
<td>6</td>
</tr>
<tr>
<td>2</td>
<td>6</td>
</tr>
<tr>
<td>3</td>
<td>6</td>
</tr>
<tr>
<td>4</td>
<td>6</td>
</tr>
<tr>
<td>5</td>
<td></td>
</tr>
<tr>
<td>Isotope</td>
<td>Half-Life</td>
</tr>
<tr>
<td>---------</td>
<td>-----------</td>
</tr>
<tr>
<td>Kr-85</td>
<td>10.7 y</td>
</tr>
<tr>
<td>Sr-90</td>
<td>29 y</td>
</tr>
<tr>
<td>Ru-106</td>
<td>368 d</td>
</tr>
<tr>
<td>Sn-135</td>
<td>2.7 y</td>
</tr>
<tr>
<td>I-129</td>
<td>1.6x10^7 y</td>
</tr>
<tr>
<td>I-131</td>
<td>8.04 d</td>
</tr>
<tr>
<td>Xe-133</td>
<td>5.3 d</td>
</tr>
<tr>
<td>Cs-134</td>
<td>2.1 y</td>
</tr>
<tr>
<td>Cs-137</td>
<td>30 y</td>
</tr>
<tr>
<td>Cs-144</td>
<td>284 d</td>
</tr>
</tbody>
</table>

TOTAL = 2.7x10^4 g

a. As calculated by ORIGEN-2
<table>
<thead>
<tr>
<th>zones of core</th>
<th>power factor</th>
<th>number of fuel assemblies</th>
<th>number of fuel rods</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1.39</td>
<td>9</td>
<td>1872</td>
</tr>
<tr>
<td>2</td>
<td>1.17</td>
<td>52</td>
<td>10816</td>
</tr>
<tr>
<td>3</td>
<td>1.07</td>
<td>44</td>
<td>9152</td>
</tr>
<tr>
<td>4</td>
<td>0.88</td>
<td>52</td>
<td>10816</td>
</tr>
<tr>
<td>5</td>
<td>0.58</td>
<td>20</td>
<td>4160</td>
</tr>
</tbody>
</table>
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To have a quantitative estimate of FP release from fuels in the accident condition such as in the case of LOCA, the spatial core heatup behavior must be evaluated considering incore distribution of fuels and their burnup conditions.

Therefore, the SHAPE has been developed with this idea to provide following analysis functions.
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(c) FP release calculation
(d) Statistical treatment of calculated results
(e) CRT Display

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The FP inventories in irradiated fuels are calculated by the ORIGEN-2 code which is built into the SHAPE code system. These calculations are virtually made by an off-line computation, and the results are arranged such that as to form a FP inventory data file comprising 11 elements (41 nuclides) which are considered hazardous if released into external environments. This data file is edited with respect to 10 burnup steps up to 40,000MWD/T, and each with respect to elapsed time during 7 days after reactor shutdown. The FP inventories in the existing core are described in three dimensional core mapping by making use of this data file, where any data falling in between those burnup and time steps are given by interpolation.
3 Basic equations for core heatup

The SHAPE evaluates spatial core heatup behaviors under accident condition, and also evaluates resulted fuel rupture and FP release into the primary cooling system, each with respect to elapsed time after the accident. The calculation of spatial core heatup is carried out for a multiple number of core regions which are specified in the initial core status, and assigning different initial power and burnup conditions to each of those regions. Thus, the heatup calculation is made for a single fuel rod representing each of the specified core regions.

As for the boundary conditions to solve the equations, the physical quantities such as inlet coolant temperature and its flow rate, inlet flow quality, saturated temperature, initial power level, reactor water level, shall be assigned to the fuel in each divided core region. In addition to basic equations, the important analytical models to evaluate physical behaviors associated with fuel heatup are briefly described in the following.

(1) Metal/Water reaction model

To evaluate the metal/water reaction (zirconium oxidation) in the process of fuel heating, Baker-Just equation(1) used to be employed in the previous calculations, but this model is said to give too conservative result especially in the higher temperature region.

Therefore, the SHAPE code now employs a following parabolic rule which virtually comprises various evaluation models with the set of parameters to be used, and a different set of reaction rate constants and activation energy shall be selected by option to represent a particular model.
Some important considerations in making use of this equation in the SHAPE code are on the various causes to restrict the metal/water reaction. The first to be considered is the existence of a thin oxidized layer on cladding surface which would already have been formed in a long range reactor operation, and the second to be considered, and perhaps more important, is the reaction restraining by the lack of steam, and by the hydrogen generation. Also, it is pointed out that metal/water reaction would no longer be considered in the SHAPE code calculation if the cladding temperature would exceed above the cladding melting point, for example 25000 K, or cladding materials be exhausted either by melting or oxidation. All these restrictive effects are taken into consideration by optional control in the code utilization.

(2) Heat transfer correlation functions

A number of heat transfer correlation functions are provided in the SHAPE code to describe various modes of convective heat transfer from cladding surface to coolant. Most of these correlation functions are already well known to many people, and therefore some important ones are selected and their names only are quoted here avoiding complicated descriptions.

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When the fuel heatup is considered up to a very high temperature range, the effect of radiative heat transfer would become increasingly important. Such is the case taking place in the TMI-2 accident under the
core uncoverity condition. Thus, the radiative heat transfers between fuel rods, and between fuel rods and other structural materials, are taken into account in the SHAPE calculation. For this, the radiative heat source is first given by the Stephan-Boltzman equation as a function of cladding surface temperature, and then the geometrical effect is taken into account by a "view factor" method so as to describe the radiation heat exchange in between the fuel lattice and its structure. The "view factor" is defined as a fraction of solid angle from a point of interest, which within its scope outlooks other radiative heat sources in the surroundings. Generally consideration of radiative heat transfer in the heatup calculation tends to give an averaging effect by smoothing the local heating profile. At the periphery of the core, these effects would then appear as radiative cooling.

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For cladding rupture in the fuel heatup process, two models are provided in the SHAPE code for optional use. One is the equivalent of the TOODEE's rupture model(2.3) which gives the rupture conditions with using such parameters as temperature and its increase rate, circumferential stress in cladding tubes, differential pressure across cladding thickness, etc.

Another mode of cladding rupture is the case which is expected to occur by thermal shock at a time of quenching. In this occasion, if cladding tube oxidation has proceeded to result in degradation of the materials, it would be likely to have cladding rupture accompanying fuel fragmentation, thus causing a sudden release of FPs into the primary cooling system. In the SHAPE calculation, this type of cladding rupture is supposed to take place at the quenching condition, particularly when the thickness of β-phase layer in the cladding would become less than 25% of the total.
4 FP release from fuel

The main objective of the SHAPE code calculation is evaluate FP release from fuels to the primary cooling system. The rate of FP release generally depends on the fuel heatup and rupture modes, and it is evaluated in the SHAPE code by two calculational steps, i.e., from the pellet to the gap, and from the gap to the primary coolant. Several analytical models are provided in the code to be used by option for various heating models. They are:

Normal operation mode : modified CESIOD model.
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MACRES CODE DESCRIPTION

1 Overview of MACRES code

The MACRES (Mechanistic computer code of aerosol and gaseous radioactive material behavior in LWR cooling system for realistical estimation of the source term) code has been developed by Japan Institute of Nuclear Safety for the Ministry of International Trade and Industry for the purpose of analyzing radioactive material behavior in light water reactor accidents.

MACRES uses the physical and chemical models based on the up-to-date knowledge from source term research. MACRES treats not only fully uncovered condition in cooling system but also water existing condition.

MACRES code consists of 120 functionally divided modules, which have easy structure to reflect the up-to-date knowledge to the code. MACRES is written in FORTRAN77 language by using SI unit and can be run by IBM compatible machine. Thermal-hydraulics conditions, plant data and mass of radioactive materials released from fuel are necessary for input.

2 Basic equation for FP transport in the primary cooling system

The MACRES analysis model divides the primary coolant system into a number of component volumes such as core, piping, pressurizer, steam generators, and calculates FP behaviors and their transport under the accident conditions. For each of those specified volumetric regions, the model considers liquid/gas phases and wall/floor geometries. The physical quantities to be treated in the basic equations are FP distributions in liquid and gas phases, in aerosol, and in wall deposition. The basic equations for FP inventory in each component volume are then given by a set of nonlinear differential equations.
3 FP behavior model in the primary cooling system

(1) Determination of FP's chemical form

After being released from fuels under core uncovery condition, the FP's chemical form which could exist in steam atmosphere shall be determined by the "free energy minimization method." In the gas phase above 600 K, assuming the chemical and thermal equilibrium to exist in the atmosphere containing FP, hydrogen and steam, the most probable combinations of chemical elements (in mol number inventories) are determined in such a way as to have the minimum total free energy in the concerned volume(1).

(2) Aerosol nucleation

During the process that evaporated FP being transported to the upper plenum region by steam flow entrainment, some of the FP having lower saturation vapor temperatures tend to attain supersaturated conditions, then to form aerosols by nucleation(2,3).

Generally, it is difficult to obtain homogeneous phase aerosol nucleation velocity for the system consisting of more than three components, and in addition the surface tension for the mixture system is unknown. Therefore the MACRES model assumes the constant number of atoms in one aerosol seed, provides the correlation table between system temperature and surface tension using saturation ratio as a parameter (by previous calculations). The surface tension values are then estimated from this table, and the aerosol nucleation velocity is obtained by using nucleation velocity equation defined for the single component system.

(3) Aerosol behavior

The aerosol particles initially formed in steam phase space of the primary cooling system would further grow by themselves due to condensation FP gas on the particle surface, and joining each other by
collision between them. Hence, the MACRES model considers the following phenomena for the process of aerosol particle growth.

- Condensation of gas onto the aerosol particles
- Brownian agglomeration
- Turbulent agglomeration
- Gravitational agglomeration

On the other hand, the aerosol particles on entrainment in the steam flow would deposit or settle on the surfaces of the primary system components, where the following phenomena are considered during this process.

- Gravitational setting
- Diffusion deposition
- Diffusiophoresis
- Thermophoresis
- Inertial collision

(4) Gaseous FP behavior

The gaseous FP other than rare gases would be removed by deposition or condensation on the wall surfaces of the primary system component structures. Also, the FP existing in gas phase would move into liquid phase by dissolution, while those in liquid phase would move into gas phase conversely. Thus, the gas/liquid distribution of rare gas is determined by using Henry's constant based on experimental data, and that of iodine is determined by using the equilibrium coefficient derived from experiments(4).

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When steam bubbles pass through water in the pressurizer, drain tank, etc., gaseous or aerosol FP in the bubbles are removed by scrubbing effect, and then move into liquid phase. The MACRES code considers the following phenomena for such scrubbing mechanism.
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Removal of gaseous FP by scrubbing effect is evaluated by determining an amount of gaseous FP absorbed or moving into liquid phase during the bubble rising (escape) period. This is obtained by considering FP mass transfer from gas phase in the bubble to the liquid phase in water, in analogy to the heat transfer correlation functions.
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REFERENCES


MELCOR Analysis of the TMI-2 Accident*

Edward A. Boucheron
John E. Kelly

Sandia National Laboratories
Albuquerque NM 87185
Telephone: (505) 844-2105

* This work was supported by the U.S. Nuclear Regulatory Commission and performed at Sandia National Laboratories, which is operated for the U.S. Department of Energy under Contract Number DE-AC04-76DP00789.
I. INTRODUCTION

The MELCOR computer code (1) is being developed by Sandia National Laboratories for the Nuclear Regulatory Commission for the purpose of analyzing severe accidents in nuclear power plants. The primary role of MELCOR is to provide realistic predictions of severe accident phenomena and the radiological source term. The results of these calculations are to be used as an integral part of probabilistic risk assessment studies. In particular, MELCOR calculations serve as a means of guiding the "back-end" analyses and can be used to answer important questions that arise in the formulation of accident progression event trees.

The first four phases of the TMI-2 standard problem (2) have been analyzed with MELCOR version 1.8.0 on a VAX 8700 computer system. The purposes of these analyses were twofold. First, while MELCOR has been used extensively to analyze BWR plants, it has not been used to analyze commercial PWR plants. Hence, one goal of the analysis was to identify PWR specific features that needed to be added to MELCOR. Fortunately, there were no major obstacles in modeling all features of the PWR system.

Second, the analysis of the standard problem allowed the models in MELCOR to be compared to plant data and to the results of more mechanistic analyses. This exercise is therefore valuable for verifying and assessing the models in the code. As will be shown, the major trends in the TMI-2 accident are reasonably well predicted with MELCOR even with its simplified modeling.

This paper describes the analyses of the TMI-2 standard problem that have been performed with MELCOR. A comparison of the calculated and measured results is presented. Based on this comparison, conclusions can be drawn concerning the applicability of MELCOR to severe accident analysis.

II. MELCOR NODALIZATION AND MODELING

The MELCOR model of the TMI-2 reactor system was developed from a RELAP5/SCDAP input deck of the system that is part of the standard problem package (2). The Initial Condition and Boundary Condition (ICBC) database (2) was also used to formulate the input deck. Relatively few nodes are employed in the model to maximize running speed while maintaining a realistic simulation of the TMI-2 accident sequence with MELCOR.

In the current analyses the first phase of the accident is simulated independently of the following phases. This approach is used to have both the first and second phases conform with the standard problem guidelines and to prevent errors in phase 1 from propagating into phase 2. Because there is currently no method within MELCOR for starting calculations with
damaged cores, or restarting calculations with altered database values, such as liquid inventory, the phase 3 and 4 calculations were run directly from the end of the phase 2 calculation. As a consequence, two different MELCOR input decks are used for the first and second phases. The major differences between the two are the initial conditions applied for phases 1 and 2, and the use of the MELCOR radionuclide package in the second and subsequent phases. The radionuclide package is required to model the transport of fission products when released from the fuel as the core degrades.

The reactor core is modeled with 42 core cells, i.e., three radial rings at 14 axial levels. The upper 12 levels are fueled, with the lower two sections representing the core support structures and lower head volume. The reactor vessel and internals are modeled with 26 heat structures. The reactor containment is modeled by one control volume.

Figure 1 shows the MELCOR nodalization of the reactor coolant system (RCS) and secondary side loops. The RCS is modeled with 11 control volumes, connected through 15 flow paths, and contains 18 heat structures. The two pumps on each loop are lumped together so that each loop has one equivalent pump. It should be noted that MELCOR does not currently contain an explicit pump model; pumps were simulated using a homologous model built with MELCOR control functions, including two-phase degradation of pump performance. The pressurizer is represented with a single control volume that in turn connects to containment through the Pilot Operated Relief Valve (PORV) drain line. The heater bundle is modeled by directly depositing power into the pressurizer water. The PORV is operated through MELCOR control functions to open at the design set pressure and latch open, thus initiating the accident sequence.

Each Once Through Steam Generator (OTSG) is modeled using five levels of heat structures, representing the tube bundles, that communicate between the RCS and secondary side control volumes. Both secondary side heat transfer loops are modeled with four control volumes, connected through four flow paths and containing six heat structures. The unique nature of the OTSG and the complex hydraulic behavior during the transient made the OTSG simulation especially challenging.

The RCS letdown and High Pressure Injection (HPI), along with OTSG Auxiliary Feedwater flow rates were modeled as hydrodynamic material sources and sinks in the lower plenum volume and are simply input as tabular functions. All of the tabular input for these quantities were taken as the suggested values from the ICBC. No attempt was made to assess the adequacy of these values with the MELCOR predicted response.

The above geometrical model was used in all four phases of the accident. The initial conditions for phase 1 were obtained by setting the reactor power, the pump speed, and the secondary side flow rate to their nominal
values and running a null transient to produce an equilibrium solution. The results of this null transient were compared to the nominal steady-state operating conditions. Slight adjustments were made until the steady-state operating conditions were satisfactorily predicted; the RCS pressures and temperatures were calculated to within a few percent of nominal operating values. This steady-state condition then served as the initial condition for phase 1.

For phase 2, the initial conditions were obtained for the standard problem package (2). The best-estimate value for the RCS inventory at 100 minutes was used as input for the code. Phases 3 and 4 were simulated by continuing calculations from the end of phases 2 and 3, respectively. In an attempt to correctly model the debris bed heating and consolidation, the effective convective heat transfer was reduced at the time of the phase 3 calculation restart through the use of MELCOR sensitivity coefficients.

The loop 2B pump transient that marks the initiation of phase 3 was simulated with a mass source to the downcomer volume and a corresponding mass sink in the loop B cold leg. Using a mass source/sink rather than the pump model allowed for direct control over the amount of mass injected. The pump transient was modeled as transferring the equivalent mass of 28 m$^3$ of liquid, over a 15 second period (2).

The initiating event for phase 4 is the relocation of debris into the lower plenum. There is currently no model in MELCOR to allow the radial migration of debris. In an attempt to simulate the relocation of debris the core support flags were reset at the restart of the calculation from the end of phase 3. The net effect of this is to convert the remaining core to particulate debris and allow relocation to the lower plenum.

III. RESULTS

A. PHASE 1 RESULTS

Phase 1 of the accident can be considered to be primarily a thermal-hydraulic transient. During this phase, the prediction of the primary system pressure is the key quantity of interest. A consideration of the system characteristics shows that the primary system pressure is a function of mass inventory and heat transfer to the secondary side. Both of these parameters are not easily determined due to the uncertainty in the letdown and HPI flowrates on the primary side coupled with auxiliary water flowrates on the secondary side.

It should be noted that MELCOR was not designed to model this type of transient in great detail because the early phase of severe accidents are not considered to have a major quantitative impact on the magnitude of the source term. Since the intended role of MELCOR is as a support calculation
tool for PRAs that can cover integrated severe accident sequences, an approximate treatment of this initial phase is considered satisfactory.

Calculating the RCS mass inventory is crucial to a correct result for phase 1 since it directly affects the system pressure. The RCS inventory is primarily dependent on the mass loss through the PORV, as modified by HPI and letdown flows. The PORV mass flow rate in turn depends on primary system pressure and the loss coefficient used for choked flow in the PORV. Our approach in building the MELCOR PORV model was to develop a model that could be benchmarked against data.

The discharge coefficients of 0.787 for steam and two-phase flow and 0.60 for liquid flow are suggested as best values for the standard problem simulation (3). Benchmark calculations were made with MELCOR and show that MELCOR predicts steam and liquid flows to be within 8 and 6 percent, respectively, of the EPRI test results for the Dresser model 31533VX-30 PORV (4). These EPRI tests were for transient operation of the PORV, as compared to steady-state calculations. The MELCOR calculations compare favorably with the test results for PORV discharge flowrate.

Not surprisingly, calculations of the full TMI-2 system model show reasonable agreement in instantaneous PORV flow rates, which in turn leads to an integrated mass loss through the PORV sufficiently accurate to model the accident sequence. At the end of phase 1 the integrated PORV loss was computed to be 126000 kg as compared to the result of 105000 kg given in reference (3). The difference can be accounted for by the fact that the calculations in reference (3) employ the homogeneous equilibrium model (HEM) for critical flow whereas MELCOR uses the Moody model. In general, the Moody model predicts higher flowrates than HEM, except near saturated liquid enthalpy. While the discharge coefficient could be adjusted to produce better results, it was determined that the current model was adequate for the simulation at hand considering that the stated accuracy of reference (3) was ±20% total inventory over the accident.

The calculated primary system pressure is compared with the TMI-2 plant data in Figure 2. For the early transient phase, the MELCOR calculations predict the system pressure reasonably well. Later in the transient some divergence of the results is seen. The underlying cause of the divergence has been determined to be the model used for the OTSG. The complexity of tube bundle heat transfer with differing heat transfer regimes cannot be easily modeled. In MELCOR, the control volumes are assumed to be well mixed so that each control volume has only one liquid temperature and one vapor temperature. If large temperature gradients should exist in the volume, then the code can only resolve these if a finer nodalization is used. Also, the heat transfer model for heat structures is relatively simple and does not include complex flow regimes that occur in each OTSG during the accident. For example, the heat structures cannot model water directly impinging on them, as the Auxiliary Feed Water (AFW) does in these heat
exchangers. The resulting model simplification cause the primary-to-secondary heat transfer to be overpredicted and lead to the underprediction of pressure.

In spite of these simplification, the pressure response is predicted reasonably well. The trends are well predicted and better quantitative agreement could be obtained by modifying certain model parameters. However, for severe accident analyses associated with risk assessment studies the current calculations are satisfactory.

There were some unresolved difficulties in using MELCOR for phase 1. The thermal-hydraulics associated with the two competing circulating flows of loops A and B coupled with increasing void in the primary system caused the calculation to be numerically inefficient. Due to the simplistic modeling of two-phase degradation the simulation of the RCS pumps became less physically realistic as the system voided. Nevertheless, calculation of the first phase was successfully completed.

In summary, the MELCOR predictions for phase 1 are in reasonable agreement with the data. The RCS inventory loss was well predicted for phase 1 and the key trends in the pressure response are predicted. Excellent quantitative agreement is not achieved due to the simplistic treatment of the primary-to-secondary heat transfer. However, for severe accident simulations for risk assessment studies, the current modeling is considered to be satisfactory.

B. PHASE 2 RESULTS

Phase 2 of the standard problem covers the period from core uncovering to initial core degradation. During this phase, one is interested in predicting the core liquid inventory, the core heating, the hydrogen production, and the cladding melting and relocation. While the data for this phase is less quantitative than in phase 1, there is sufficient information to perform an assessment of the core degradation modeling.

The results for phase 2 show reasonable agreement with the available data. Table 1 lists the timing of key events during this phase. The timing of most events is relatively good. However, the fuel rod rupture time is predicted early and this is most likely due to the simplified treatment of this model. The hydrogen production is calculated to occur over a prolonged period. This is due in part to an intentional reduction in oxidation rate during the calculation through MELCOR sensitivity coefficients. It was found through sensitivity studies that reduced oxidation rates lead to smoother hydrogen production which in turn lead to improved thermal-hydraulic prediction and calculational performance.
As in phase 1, the prediction of primary system pressure is found to be very sensitive to the primary-to-secondary side heat transfer. The primary system pressure is plotted along with the TMI-2 plant data in Figure 3 and is found to be in good agreement with the data. In phase 2, the production of hydrogen in the core leads to a significant degradation of the primary-to-secondary heat transfer. This is because the hydrogen "blankets" the tube side of the OTSG and prevents flow through the steam generator. Furthermore, the production of noncondensible gas (hydrogen) leads to higher pressure. The good agreement here indicates that the timing of hydrogen production and the effect of hydrogen production on heat transfer are being predicted well. Sensitivity studies confirm this conclusion. Later in phase 2 the pressure is underpredicted. There appear to be two reasons that partially account for this. First, due to both the hydrogen partial pressure contribution and steam generator "blanketing", the rate and total amount of hydrogen production is crucial to system pressure prediction. Secondly, it appears that at the end of phase 2 other phenomena may have been occurring that are not adequately documented and therefore modeled such as localized core debris "slumping" or "dripping" that led to rapid steam generation. There is a large pressure increase observed that is at least partially accounted for by the operation of the 2B main coolant pump. This can be seen at the latest times plotted in Figure 3. Since this defines the end of phase 2 and start of phase 3 it is unclear how to interpret data at the very end of phase 2.

The fuel assemblies exhibit dryout, subsequent heating, cladding oxidation, cladding rupture, and melting and relocation in the upper portions of the core by the end of phase 2. The lower levels in the core show varying degrees of heating and oxidation. The calculation shows that the core gradually uncovers and the liquid level reaches a lower limit of approximately 1.3 m above the bottom of the core, exposing the upper 2.4 m of the core to steam. The core liquid level (swollen) level as a function of time is shown along with the downcomer liquid level in Figure 4.

The initial fuel rupture was calculated to occur at 7700 seconds after the beginning of the accident. The plant data indicate that this event occurred at about 8200-8400 seconds and the computed best-estimate value is 7900 seconds (2). MELCOR predicts rupture early, however, if the rupture temperature criterion were 100 K higher, the calculated rupture time would be approximately 300 seconds later. If one considers uncertainty in the rupture model, then the results are in reasonably good agreement with the data. Again, this indicates that the core heating is being adequately modeled with MELCOR.

Figure 5 shows the time history of hydrogen production, which begins around 7600 seconds and rapidly increases around 9000 seconds. At this point "blanketing" of the heat exchangers should be fully established. The total calculated hydrogen production is 225 kg. This value is in good agreement with the standard problem package value (~200 kg (2)).
MELCOR predicts core relocation (i.e., candling and particulate debris formation) beginning around 8800 seconds. There is a MELCOR core model that simulates the hold-up of molten Zircaloy behind the ZrO$_2$ shell. The effective release temperature of the oxide shell is 2500 K by default and relocation cannot therefore begin until this temperature is exceeded. Figure 6 shows the time history of fuel temperatures at five axial levels for the inner radial ring, indicating the axial temperature variation in the core. The maximum core temperatures at the end of phase 2 are about 2900 K. Table II shows the average component temperatures and surrounding fluid temperatures through the core at the end of phase 2. Axial levels are numbered 3 through 14 from the bottom of the active core upwards. Radial rings are numbered 1 to 3 from inner to outer. Approximately 35% of the core has been degraded and 25% of the total core Zircaloy has been oxidized at this point in time. Obviously, the upper regions of the core are predicted to be heavily oxidized. The degraded state of the core is represented schematically in Figure 7. There is significant radial deviation in damage state. This is due, at least in part, to the radiation model employed. Within the current MELCOR core model only global radiation view factors are used for each core cell and structure, whether intact or debris. To represent core structure and debris radiation heat transfer in some reasonable manner requires using a compromise value for the overall cell view factor. This modeling limitation also accounts for some of the high local temperatures predicted for the inner radial ring, as exhibited in Table II.

The only significant problem encountered in the phase 2 analysis was calculating the correct response of the pressurizer. It was found that a delicate balance exists between core pressure and pressurizer level. If the core pressure falls too low, then the pressurizer can empty and terminate the accident. In the initial MELCOR calculations for this phase the pressurizer level was treated as a boundary condition beyond 9000 seconds and was not allowed to empty. In subsequent calculations, when the core degradation models were operating in the proper time frames, this boundary condition was eliminated and the code calculated that the pressurizer did not drain. Figure 8 shows the calculated pressurizer level as compared to the plant data. The MELCOR calculations do indicate good agreement; the pressurizer does not drain, but rather is held back by a positive pressure difference from the primary system. This is due in large part to the rate of hydrogen production, as alluded to in earlier discussion. The heat transfer to the secondary side at this time is reduced due to the blanketing effect of hydrogen. This reduction leads to a higher primary system pressure. An improved model for the effect of hydrogen will likely reduce the heat transfer and lead to higher core (and primary) pressure and further reduce the draining that takes place late in phase 2 and concurrently improve the RCS pressure prediction.

In summary, the MELCOR simulation of phase 2 is quite good. The timing of key events is good. Hydrogen production and the state of the core at the
end of phase 2 are in reasonable agreement with the estimates found in the standard problem package. This agreement shows that core degradation modeling in MELCOR is applicable to severe accident analysis. Although the standard problem does not include comparisons of fission product releases the calculations are done as a matter of course in MELCOR.

C. PHASE 3 AND 4 RESULTS

During phase 3 there are several computationally challenging events taking place: the loop 2B pump transient, core debris heatup and consolidation, and recovery of liquid level over the top of active fuel. In terms of the ability to calculate the basic thermal-hydraulics associated with the pump transient and recovering of the core MELCOR performed well; there were no great computational problems. The difficulties in the simulation of phase 3 and 4 were due to the simplified debris heat transfer models that exist in the current MELCOR code.

Figure 9 shows the predicted RCS pressure during phases 3 and 4. It is clear that the calculation is not following the trends show in the data. There are at least two reasons for this behavior: 1) The hydrogen blocking model is insufficient, as discussed in the phase 2 results. 2) The simplified debris heat transfer models are inadequate. All hydrogen production ends at the loop 2B pump transient. This is because there are no reflod phenomena related hydrogen production models in MELCOR such as a core-shattering-rapid-oxidation model. The pump transient initiates a high steaming rate that serves to cool the existing debris and core structures below rapid oxidation temperatures. This cooling effect precludes any subsequent hydrogen production in phases 3 and 4 and accounts for some of the underprediction in RCS pressure seen.

The MELCOR debris models are lumped-parameter heat transfer calculations employing a single temperature for all debris at any particular axial level in the core model. The convective correlations are for single spheres in an infinite medium, not packed beds. A simple boiling model that is employed globally in the core is applied to the debris without any consideration of bed dryout. A stratified structure with steep temperature gradients, such as existed at TMI-2, is difficult to represent. Since MELCOR can only resolve stratification to the level of core nodalization with simple lumped parameter models it is not surprising that the code does a poor job representing the thermal response of debris during this phase.

Table III presents the core thermal state at the end of phase 3. It is clear that the core debris and remaining structures are all near liquid saturation temperature; the debris has cooled. The core geometry at the end of phase 3 is essentially the same as at the end of phase 2 because of the cooling of debris and core structures.
Figure 10 shows the core, downcomer and upper plenum liquid levels for phases 3 and 4. The initial surge in core level from the pump transient is apparent at 10500 seconds, but drops off due to boiling. The core liquid level again increases in the 12000-12500 second time frame with a corresponding pressurization that can be seen in Figure 9. After 12500 second the core is completely covered with liquid.

Although the calculation was continued through phase four, the effort is somewhat in vain. An inspection of Table III reveals that there is no molten debris at the end of phase 3 to relocate to the lower plenum volume as in the accident. The MELCOR phase 4 calculation therefore consists of relocating the hot solid core debris to the lower plenum.

IV. CONCLUSIONS

The MELCOR 1.8.0 computer program has been shown to be capable of modeling the TMI-2 standard problem for the first four phases of the standard problem exercise, covering the initial 230 minutes of the accident. Although improvements are still needed for various models in MELCOR, the calculations are capable of simulating the course of events in the TMI-2 accident.

In phase 1, the MELCOR predictions are in reasonable agreement with the data. The key trends in the pressure response and the inventory loss are well predicted. Excellent quantitative agreement is not achieved in pressure due to the simplistic treatment of the primary-to-secondary heat transfer. However, for severe accident simulations for risk assessment studies, the current modeling is satisfactory.

In phase 2, the MELCOR analysis is quite good. While the timing of some events is slightly incorrect, the general trends are very good. Hydrogen production and the state of the core at the end of phase 2 are in reasonable agreement with the estimates found in the standard problem package. From these results it can be concluded that the core degradation modeling in MELCOR is applicable to severe accident analysis.

The phase 3 and 4 calculations demonstrate that MELCOR is capable of handling recovered core sequences, even if in a limited manner; more sophisticated core debris and relocation models are required to correctly represent the true events that took place in the TMI-2 accident.

One particular outcome of this analysis effort is demonstration of the ability of MELCOR to analyze severe accidents in PWR plants. With future code development efforts, guided in part by this work, the ability of MELCOR to simulate, with confidence, the full range of LWR accidents will be greatly improved.
V. REFERENCES


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* Set to ICBC Value

Table 1.

Timing of Key Events During Phase 2.
EDIT OF CORE CELL AVERAGE TEMPERATURES (K)

TOP NUMBER IS AVERAGE COMPONENT TEMPERATURE
BOTTOM NUMBER IS CHANNEL FLUID TEMPERATURE

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Table II.
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<td>544.31</td>
<td>544.31</td>
</tr>
</tbody>
</table>

Table III.
Figure 4

- Core
- Downcomer
Figure 5

Hydrogen Mass (kg)

Time (s)
Figure 7

RADIAL RING

3.7 m from Bottom of Core

AXIAL LEVEL

1.2 m from Bottom of Core

KEY for Core Geometry

- Empty
- Debris
- Oxidized with Debris
- Intact with Debris
- Intact
Figure 10

[Graph showing time and levels for different sections: Upper Plenum, Core, Downcomer]
Computer Code Description: MELCOR

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I. INTRODUCTION

The computer code used in the TMI-2 standard problem accident analyses was MELCOR, version 1.8.0. The MELCOR computer code (1) is being developed by Sandia National Laboratories for the Nuclear Regulatory Commission for the purpose of analyzing severe accidents in nuclear power plants. The primary role of MELCOR is to provide realistic predictions of severe accident phenomena and the associated radiological source term. The results of these calculations are to be used as an integral part of probabilistic risk assessment studies.

II. MODELING OVERVIEW

MELCOR is a fully integrated code that models all phases of the progression of severe accidents in light water reactors. An entire spectrum of severe accident phenomena and important reactor systems is modeled in MELCOR covering both in-vessel and ex-vessel processes. MELCOR uses a combination of parametric and mechanistic modeling to treat the complex phenomena expected during a severe accident. This type of treatment allows for realistic modeling of the key phenomena and at the same time allows for reasonably fast run times.

A principal application of MELCOR is the support of PRA in which calculations must be performed for several plausible accident scenarios. Therefore, one of the objectives of MELCOR was minimum execution time (1 to 2 Cray hours) for a full accident sequence calculation. Integration of models representing the full spectrum of characteristics of LWR severe accident progression into a single computer code combined with a requirement for minimal execution time demanded a compromise of phenomenological detail in some areas. As a result, MELCOR does not apply the detailed modeling approach taken in "mechanistic" computer codes such as TRAC-MELPROG (2); yet in most areas, MELCOR represents an advancement in modeling detail from other severe accident analysis tools such as Source Term Code Package (3) and MAAP (4). However, there are areas in MELCOR where the distinction in modeling detail is not very pronounced. For example, selected MELCOR packages are virtually identical to their mechanistic counterparts (e.g., CORCON (5) for modeling core-concrete interactions).

MELCOR either explicitly models or parametrically treats key in-vessel and ex-vessel phenomena together as an integrated calculation. Characteristics of severe accident in-vessel phenomena that are treated in MELCOR include the two—phase thermal—hydraulic response in the reactor coolant system, thermal response of structures, fuel rod heating, Zircaloy oxidation and hydrogen generation, core degradation processes, debris bed behavior, and
lower head response. In addition, fission product release, transport, deposition, and revaporization are also treated (in both vapor and aerosol forms).

A corresponding set of ex-vessel phenomena is also treated. In particular, MELCOR treats core-concrete interactions, containment and auxiliary building thermal-hydraulic response, containment heat structure response, hydrogen burning and detonation, aerosol behavior, and the impact of engineering safety features on thermal-hydraulics and radionuclide release and transport.

For the user, the approach used in developing MELCOR has been to build generic "building-block" models that the user in turn manipulates to form plant specific components. For example, a steam generator is modeled by the user as a combination of heat structures and control volumes. This feature gives the user great flexibility in developing a model that represents plant specific features without modifying the code internals. In addition, the nodalization is specified through user input. This allows the user to model areas of little interest with little or no detail, whereas areas of greater interest can be finely nodalized.

III. CODE STRUCTURE

The MELCOR code is modular in structure; the FORTRAN representation of process models is separated into several distinct "packages" each of which addresses a well defined group of closely related phenomena. This architecture facilitates the incorporation of additional or alternative phenomenological models.

A unique two or three character alphanumeric identifier is assigned to each package and is incorporated in the names of its subroutines, common blocks and input identifiers. Most of the packages are normally inactive (i.e. not executed) unless the user activates them. This is accomplished by simply including input information for that package in the input deck; the identifier field of each line of input specifies the destination package. The following summaries describe the MELCOR packages:

BUR: Combustion of Gases — Initiates and propagates deflagrations involving hydrogen and/or carbon monoxide. Calculates burn completeness and flame speed. Models in this package are based on those in the HECTR code (6).

CAV: Core Concrete Interactions — CORCON/MOD2 (4) with enhanced sensitivity analysis capabilities.
CF: **Control Functions** — Evaluates “control functions” (user defined functions of MELCOR calculated variables) and applies them to define or control various aspects of the computation, such as opening and closing valves, defining new plot variables, etc.

COR: **Core Behavior** — Evaluates the behavior of the fuel, cladding and other core and lower plenum structures, including heat up, oxidation, melt formation and relocation, reactor vessel failure and ejection of core materials to the cavity.

CVH: **Control Volume Hydrodynamics** — Evaluates, in conjunction with the flow path (FL) package, mass and energy flow between hydrodynamic control volumes.

CVT: **Control Volume Thermodynamics** — Evaluates the thermodynamic state within each control volume for the CVH package.

DCH: **Decay Heat** — Used by the other packages to evaluate heat generated by the decay of radionuclides.

EDF: **External Data File** — Allows the user to either read or write data into or out of a calculation.

ESF: **Engineered Safety Features** — Models the thermal-hydraulics of fan coolers.

FDI: **Fuel Dispersal Interactions** — Models ex-vessel debris heat transfer during fuel coolant interaction.

FL: **Flow Path** — Models, in conjunction with the CVH package, the flow rates of gases and liquid water through pathways that connect hydrodynamic volumes.

H2O: **Steam Properties** — Evaluates the thermodynamic properties of steam based on the Keenan and Keyes equation of state.

HS: **Heat Structures** — Evaluates thermal response of structures considering conduction, condensation, convection and radiation heat transfer along with mass/heat transfer between structures and hydrodynamic volumes. Includes a model for degassing of concrete.

MP: **Material Properties** — Evaluates the physical properties of materials used in other packages, except for the thermodynamic properties of steam and noncondensable gases.

NCG: **Noncondensible Gas** — Evaluates the properties of noncondensible gas mixtures using an equation of state that is based on the JANAF thermochemical data.
RN: **Radionuclide** — Models fission product behavior; release from fuel and debris, aerosol formation, transport and deposition, removal of fission products by ESFs. Allows for simplified chemistry.

SPR: **Containment Sprays** — Models the mass and heat transfer rates between containment spray droplets and hydrodynamic volumes through which they fall.

TF: **Tabular Function** — Evaluates user specified “tabular functions” to define or control various aspects of the computation such as mass and energy sources, integral decay heat level, etc.

TP: **Transfer Processes** — Controls the transfer of core debris and radionuclide classes from the COR package to other packages.

IV. **CORE DEGRADATION MODELS**

The process of core degradation is of interest to severe accident simulation in general and phase 2 of the TMI-2 standard problem in particular. Oxidation of the core Zircaloy and steel is calculated using standard parabolic kinetics, with appropriate rate constant expressions for the materials, and limited by steam availability. A user-specified criterion is used to transform intact fuel rods into particulate debris. Particulate debris is also subject to oxidation, but will typically have a reduced surface-to-volume ratio for oxidation.

There is no explicit model for cladding ballooning in MELCOR. Cladding rupture is modeled as occurring when either the cladding temperature exceeds a given threshold temperature (default 1200 K (7)) or the cladding melts. When an individual core cell is determined to fail through either criterion, the fission gas plenum and gap inventories of radionuclides for the entire fuel rod (and all rods of this type in a particular radial ring) are released to the appropriate core control volume.

The release of fission products from fuel is modeled using CORSOR-M (8), which gives an Arrhenius form of the release equation with constants based on experimental data. These rate equations are then modified for the appropriate surface area to volume ratio of the fuel/debris as compared to the ratios represented in the experiments.
V. SUMMARY

The TMI-2 standard problem accident was analyzed using MELCOR version 1.8.0. MELCOR is a fully integrated code that models all phases of the progression of severe accidents covering both in-vessel and ex-vessel processes. MELCOR has been used to simulate a variety of severe accident sequences in BWRs and now, to a limited degree, in PWRs. In addition, there have been limited verification and validation studies assessing the code against experimental data.
VI. REFERENCES


SCDAP/RELAP5 DEMONSTRATION CALCULATION OF THE TMI-2 ACCIDENT\textsuperscript{a}

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\textsuperscript{a} Prepared for the U.S. Department of Energy under DOE contract NO. DE-AC07-76ID01570.
1. INTRODUCTION

Described in this report are the SCDAP/RELAP5\textsuperscript{1} calculations performed by the TMI-2 Accident Evaluation Program (AEP) to demonstrate that sufficient data exist to establish a standard problem based on the TMI-2 accident. Other purposes of the calculations are to benchmark the SCDAP/RELAP5 integrated severe accident analysis code, and to enhance the AEP's understanding of the TMI-2 accident phenomenology. Although the TMI-2 accident was not a controlled experiment, it provides a singular data base for a full scale, integrated light water reactor that suffered extensive core damage. Experiments conducted following the TMI-2 accident have been typically small scale separate effects tests with damage limited to the early phases of core damage progression. Scale effects and feedback effects between extensive core geometry changes and core thermal-hydraulics are not present to the extent they likely were during the TMI-2 accident. The TMI-2 accident provides a significant test of the severe accident analysis codes.

The nodalization used for the SCDAP/RELAP5 calculations are shown in Figure 1-1. The TMI-2 plant is modeled with two loops where the cold legs have been combined as one effective cold leg for each loop. The reactor core is represented by 30 RELAP5 volumes and 3 SCDAP fuel rods and 3 SCDAP control rods with 10 axial nodes each. As shown in Figure 1-2 the core is modeled using 3 radial rings.

The results of the INEL SCDAP/RELAP5 calculations and the conclusions to be drawn from the calculations are discussed in the remaining sections.

2. RESULTS

It is desirable that the code calculations match recorded data or independent estimates of the parameter. For an integrated severe accident analysis code such as SCDAP/RELAP5, the code should be capable of matching the thermal-hydraulic, core heatup, core melt progression and end-state geometries.

2.1 Phases 1 and 2

2.1.1 RCS Pressure

The first parameter of interest is the PORV flow rate. Shown in Figure 2-1 are the integral PORV flows calculated by Nomura, the SCDAP/RELAP5 calculation using the boundary conditions discussed in Section 3, and the SCDAP/RELAP5 flow with the PORV flow area adjusted to match Nomura's calculation. Examination of the figure clearly shows that the SCDAP/RELAP5 calculation without the corrected flow area under estimates the total PORV flow. Since the HPI/makeup boundary condition is based on Nomura's estimate of the PORV flow, the results presented in the remainder of this report are based on the adjusted PORV flow area.

Pressure provides an overall indication of the state of the RCS. The calculated and measured RCS pressure are shown in Figure 2-2. The base case calculation fails to match the increase in pressure that begins at 125 min. As a direct result, the pressurizer drains as shown in Figure 2-3. That in turn effectively terminates core heatup and the progression of the accident. On the assumption that this effect is related to the HPI/makeup boundary condition the HPI/makeup rate was reduced to 2 kg/s for the time period 100 to 174 min. As shown in Figure 2-2 the parametric case matches the measured pressure more closely. It is our opinion that the parametric case is well within the uncertainties in the HPI/makeup mass flow rate.

2.1.2 Pressurizer Level

The calculated and recorded pressurizer levels are shown in Figure 2-3. The calculated level matches the data quite well up to 100 min. At 100 min, the last RCS pump tripped, causing the remaining liquid in the RCS to settle in the reactor pressure vessel and the steam generators. There are two distinctly different periods following the final pump trip. The first period (100 to ~139 min) shows a steady decrease in level. The second period after closure of the PORV block valve indicates a nearly constant level. The behavior of the level during those two time periods is due to distinctly different phenomena.

The pressurizer configuration relative to the hot leg is shown in Figure 2-4. First, the surge line configuration has the potential to form a loop seal between the pressurizer and hot leg. Second, during periods of steam flow inflow to the pressurizer there is a potential for counter current flow at the surge line to pressurizer junction. While Anderson\textsuperscript{6} argues that the liquid outflow from the pressurizer was counter current flow limited, that is steam was flowing into the pressurizer and liquid flowing out with the liquid flow limited by the interphase drag. The code calculates co-current flow, that is liquid entrained in the steam flow. The surge line is calculated to void shortly after the pump trip. Following surge line voiding liquid is
Figure 1-2. SCDAP radial core nodalization.
held up in the pressurizer simply by the interphase

\( q \) associated with the inflow of steam to the

pressurizer. The only phenomena left to cause the

level decrease in the calculation is liquid in the

pressurizer flashing to steam. The effect of flashing in

the SCDAP/RELAP5 calculation can be estimated from

the calculated vapor generation rate for the five

pressurizer volumes. The liquid level is estimated

using:

\[
L(t) = L(103) - \int \sum_{i=1}^{5} \frac{V_i \gamma_i}{\rho_i} \, dt
\]

where,

\( L(103) \) = pressurizer level at 103 min

\( i = i^{th} \) pressurizer volume

\( \gamma_i \) = vapor generation rate per unit volume

\( V_i \) = volume

\( \rho_i \) = liquid density

\( \Lambda_i \) = flow area

Application of the above equation to the

SCDAP/RELAP5 calculation produces the estimate of

the level shown in Figure 2-5. The level calculated

from the vapor generation rate compares quite well to

the level measurement simulation. Thus, it is

reasonable to conclude that the calculated level decrease

due to flashing in the pressurizer. However, there is

insufficient plant data to conclude that flashing was the

sole cause of the measured level decrease.

At PORV block valve closure, there is no

longer a driving force to pull steam through the

pressurizer and maintain the liquid level. Liquid flows

out of the pressurizer and forms a loop seal in the

surge line. The amount of liquid required to refill

the surge line to the hot leg entrance is equivalent to

\(-0.17 \) m of pressurizer liquid level. In fact, the code

calculates a level drop of \( 0.16 \) m for the parametric

HPI/makeup case. The data indicates a level drop of

\( 0.24 \) m occurring at 145 min. If the code and the

recorded data are descriptive of the same phenomena a

number of conclusions can be drawn: (a) the code is

performing reasonable well in predicting the level
drop; (b) the data suggests that more liquid flowed

out of the pressurizer than was required to form the

loop seal; and (c) the PORV block valve may have

been closed 6 min later than indicated by other plant

For the parametric HPI/makeup mass flow rate,

RCS pressure remains above the pressurizer steam
dome pressure and a stable liquid level is possible.

For the base case boundary conditions, RCS pressure
decreases below the pressurizer steam dome pressure

and additional liquid is forced into the hot leg. This

provides cooling in the hot leg and RCS pressure

continues to decrease. Thus, an unstable condition is

calculated to exist, which causes the pressurizer to

drain and cool the core. The significant difference

between the two cases is the oxidation rate for the

parametric case is on average about three times greater

than in the base case by the time the PORV block

valve is closed. Thus, the rate of energy input to the

RCS is significantly greater for the parametric case.
Figure 2-1. Comparison of PORV outflows.

Figure 2-2. Comparison of RCS pressures
Figure 2-3. Comparison of pressurizer levels.

Figure 2-4. Schematic of surge line arrangement.
Figure 2-5. Pressurizer level measurement simulation compared to level decrease caused by flashing.

2.1.3 Core Damage Progression

Since cladding temperatures were not measured, indirect measurements must be used to gauge the accuracy of the temperature calculations. Examination of Figure 2-6 reveals that the core uncovers at approximately 115 min, which is in reasonable agreement with the recorded measurement of hot leg superheat at approximately 113 min. Twenty-two minutes after the start of core heatup (137 min), SCDAP/RELAP5 predicts the first rod rupture due to ballooning. Thus, the fission product gases in the fuel rod gap would be released and a pathway to the reactor building exists through the stuck-open PORV.

The timing of calculated rod rupture is consistent with the initial rise in the reactor building air sample radiation monitor particulate channel at approximately 135 min. Rapid oxidation is predicted to begin at about the time of rod rupture. Oxidation then drives the core temperatures and RCS pressurization. As shown in Figure 2-6 core heatup starts at the top and progresses downward. Oxidation tends to cause uniformity in the axial temperature profile. The code assumes failure of the cladding oxide shell and relocation of molten core materials to lower, cooler regions of the core at 2680 K. Relocation first occurs in the center core region at -152 min. The sharp decrease in the temperature of the 1.7 m and 2.3 m nodes is the result of molten ZrO₂ and UO₂ slumping downward and mixing with cooler material.

Another indication of the core damage progression is the prediction of total hydrogen generated by cladding oxidation. As shown in Figure 2-7 rapid oxidation was calculated to begin at about 140 min and progresses at a rate of about 11 kg/min to 160 min. At about 160 min, oxidation is calculated to essentially cease as there is no longer any oxidizable Zr available in a region of the core in contact with steam at a temperature to support rapid oxidation. At the same time (see Figure 2-1), the rate of RCS pressurization decreases significantly. Based on the recorded pressure response the calculated core damage progression is clearly inconsistent with the accident progression as it occurred. The calculated total hydrogen generated by 174 min is about 240 kg, which is 60 kg less than estimated by Kuan and Henri. There are a number of possible sources of error in the total hydrogen and core damage progression calculation. The quantity of molten Zr calculated to relocate may be overestimated. Excessive relocation could be related to the oxide film failure criteria or over estimation of cladding temperatures. The lower crust is calculated to occur at -1.2 m (4 ft). Thus, the amount of Zr available for oxidation in a hot region of the core is reduced by about one-sixth of the Zr inventory. The observed core end-state includes a number of standing (essentially undeformed) fuel assemblies in the core periphery. The current input model results in predictions of core damage across the
Figure 2-6. Calculated center region temperatures.

Figure 2-7. Calculated hydrogen generation.
entire core radius. Increasing the number of radial rings in the outer region of the core is expected to refine the core damage predictions, and potentially make more Zircaloy available for oxidation later in the transient. A sensitivity case was run with increased radial (4 rings) and axial nodalization (12 levels). The renodalization had little effect on the total hydrogen or Zircaloy availability at the end of phase 2. However, the change was significant enough to indicate that convergence had not been reached.

The core collapsed liquid level, shown for the center region in Figure 4-8, drops to a near constant value of ~1 m. At about the time of significant relocation of molten core materials, the level drops to near the bottom of the core. As indicated by the core end-state, the minimum water level is believed to have been about 0.6 m (2 ft). This tends to indicate, at least in terms of this calculation, that the HPI/makeup rate might be somewhat greater than 2 kg/s.

![Figure 2-8. Calculated core collapsed liquid level (center region).](image)

Thus by 174 min the code predicts the formation of a molten pool; a lower crust to retain the molten pool in the core region, hydrogen generation of 240 kg, and minimum liquid level near the bottom of the core. Figure 4-9 shows the core geometry at 174 min. The predicted geometry is generally consistent with Figure 1-1, although there are a number of differences, such as a calculated height of the lower crust at the center of the core that is higher than the observed height.

![Figure 2-9. Calculated core geometry at 174 min.](image)

2.1.4 Structure and Hot Leg Temperatures

Examination of the TMI-2 reactor vessel has resulted in the conclusion that little damage occurred to upper plenum vessel structures, except for the underside of the upper grid plate. Calculated temperatures for the upper grid plate, upper plenum structure, top of the candy cane piping, and the surge line are shown in Figure 4-10. The code calculates the upper plenum and hot leg piping to stay relatively cool. But the upper grid plate is heating to above its melting temperature, with a temperature rise rate that parallels core oxidation. Up to the time of PORV block valve closure, the surge line pipe is calculated to heat up at about the same rate as the upper grid plate. At PORV block valve closure the re-entrant liquid, (section 4.1.2), cools the piping to the saturation temperature. The calculation is consistent with the fact that the surge line did not fail and calculations for other accident sequences predict surge line failure. If the PORV block valve had remained open, extrapolation of the calculation indicates that the code would predict surge line failure.
2.2 Phases 3 and 4

Kuan estimated that ~160 kg of hydrogen were generated during the pump transient, or the equivalent oxidation of the upper half of about 96 fuel assemblies. Oxidation is hypothesized as the driving force for the continued rapid pressurization of the RCS during the pump transient. SCDAP/RELAP5 calculates that 5 kg of hydrogen was generated between 174 and 190 min. Thus, the pressurization shown in Figure 4-12 is entirely due to stored energy removal from the core. The significantly smaller pressure increase during the pump transient is consistent with the lack of oxidizable Zircaloy in the hot regions of the core. The possible sources of error in the calculation of Zircaloy availability were presented in section 4.1.3. Without an accurate calculation of Phase 2, the results of calculations for Phases 3 and 4 are likely to be significantly different from the accident data.

In the following section, we present the calculated response of the system to the 2B RCS pump transient. The calculated system response and damage progression provide the set up for the pump transient.

In Figure 2-11, the calculated A-loop hot leg temperatures are shown. The calculated temperatures are lower than the measured temperatures, which may be due to uncertainties in the boundary conditions or the code models used.

3. CONCLUSIONS

While there are uncertainties in the boundary conditions, it is possible to simulate the TMI-2 accident. That is more easily accomplished earlier rather than later in the accident sequence. The effect of uncertainties in boundary conditions, code models and the input model appear to collect and cause increased variance from the actual accident state trajectory as the calculation progresses. There is a need for improvements in (a) the data base for late phase core melt progression (late phase 2 through core melt.
relocation to the lower plenum), (b) the requirements for input deck nodalization, and (c) the level of accuracy required for boundary condition specification.

Specific component responses, such as the pressurizer from 100 to 174 min, are of great interest. While SCDAP/RELAP5 did not match the data exactly, the trend of the data was well matched. The code does predict hold up of liquid in the pressurizer after PORV closure as it was measured. While Anderson uses counter current flow to explain the level decrease during 100 to 145 min, the code calculates the level decrease entirely on flashing within the pressurizer. There is insufficient accident data to resolve the different hypotheses. The fact that the code predicts under some conditions, as in the base case, draining of the pressurizer following PORV block valve closure is not necessarily indicative of a real world situation. Since the pressurizer contains a volume of water that could be available to cool the core, as in the base case, an understanding of whether the code is simulating real phenomena should be developed. An experimental data base is required to develop the needed understanding.

Overall, the TMI-2 accident provides a significant stress test for severe accident analysis codes from the beginning of core heatup to in-vessel relocation of the molten core.
REFERENCES


Appendix M

Technical Research Center of Finland
MAAP 3.0 CALCULATIONS FOR THE PHASES 1 AND 2

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I. INTRODUCTION

The first 174 minutes (phases 1 and 2) of the TMI-2 accident were calculated by using the Modular Accident Analysis Program (MAAP), Version 3.0 at the Technical Research Centre of Finland. A sequence of MAAP codes have been developed by Fauske and Associates, Inc. (FAI), of which the version 3.0 was released in 1986. MAAP is an integral code that includes models for all the important phenomena which might occur within the plant in a severe accident. One of the goals of the program has been fast execution time, consequently quite simple modeling is used for some of the phenomena.

MAAP 2.0 input described in the EPRI report (1) was utilized as a basis for construction of the MAAP 3.0 input, augmented with information from the preliminary MAAP 3.0 B calculations (2). Our MAAP 3.0 input was later commented by A. Sharon from FAI, and two significant errors was pointed out:

- steam superheat was too low,
- pressurizer was attached erroneously to the B-loop due to the misleading MAAP 3.0 input instructions.

MAAP 3.0 B had replaced MAAP 3.0 at VTT at the time of receiving the FAI's comments, and hence the MAAP 3.0 calculations were not remade. Effects of the incorrect pressurizer location was later studied by the MAAP 3.0 B code. The error did indeed affect the calculation from 73 minutes to 100 minutes when the system was highly unsymmetric, but the effect on heatup phase was surprisingly small. Too low steam superheat probably caused overprediction of the primary system pressure in the beginning of the accident. All results described hereupon refer to the original (corrected) MAAP 3.0 calculations.

II. Results

A. Node Diagram

MAAP assumes a fixed nodalization of the primary system. A schematic of the primary system nodalization model for B&W plants is shown in Fig. 1. The code has separate models for the so-called "broken" (=B) and "unbroken" (=A) loop. The number of control volumes is small compared to detailed thermal-hydraulic codes.
MAAP thermal-hydraulic nodalization is further simplified by the convention that water pools are separately accounted for in the cold legs, intermediate legs and downcomer only. The horizontal parts of the cold legs and water in the lower head are included in the downcomer water pool. The nodalization shown in Fig. 1 is used as such in gas transport calculations.

B. Discussion of Results

The base case

A summary of the calculated sequence of events is presented in Table 1. The calculated primary system pressure is compared with the measurements in Fig. 2. A common feature of all the VTT's MAAP 3.0 calculations was an overprediction of the primary system pressure by about 100 psi (0.7 MPa) partly due to the incorrect steam superheat. The pressure drop at 160 minutes was caused by dumping of the pressurizer water into the primary system.

The calculated pressurizer water levels shown in Fig. 3 resembled the MAAP 2.0 results (1). A sequence of drops was seen on the calculated water level. The first drop occurred at 73 minutes after idling the B-loop, the second after restoration of the AFW to the A-loop (99 min) and subsequent primary system cooling. The third drop at 160 minutes resulted in dumping of all pressurizer water into the primary system.

Fig. 4 shows the calculated core water level in the base case. Core uncovering was calculated to begin at 106 minutes, when water level decreased below 6.12 m (measured from the bottom of the RV head). The calculated minimum level was 3.4 m above the bottom of the RV head (0.95 m above the bottom of the fuel rods), predicted to occur at 160 minutes.

The calculated cladding temperatures at the core center are shown in Fig. 5. Heatup of the uppermost nodes began after core uncovering at 106 minutes and rapid escalation of the temperatures began at 140-150 minutes. The outer radial nodes did exhibit similar characteristics, except that fuel melting began about 10 minutes later. All nodes above the core midplane reached the assumed eutectic melt temperature, 2500 K, resulting in major relocation of core materials into the core
bottom nodes (nodes 2 and 3). The calculated core temperatures and material distribution at 174 minutes is presented in Tables 2 and 3.

An integrated release of about 240 kg of hydrogen was estimated by the end of phase 2. Most of the hydrogen was produced during the relatively short interval between 140 and 155 minutes, as shown in Fig 6. Hydrogen production was terminated by the blockage of the core at 155 minutes, after which steam could not enter the hot core regions.

Timing of the fission product release resembled hydrogen production. A release of about 75% of the noble gases, Csl and CsOH was calculated by the end of phase 2, whereas release of other fission products was small. Pressurizer PORV was closed at 142 minutes, 1-2 minutes after major fission product release from fuel was calculated to begin. Hence, only a small fraction (2*10^-5 of Csl inventory, for example) was transported from the primary system to the containment.

Parameter variations

Seven sensitivity runs were calculated by varying the rate and timing of the HPI as recommended in EGG-TMI-7833 (3). The HPI rates used after 100 minutes in the sensitivity runs are shown in Table 4.

Increase of the HPI rate after 100 minutes from 4 to 5 kg/s delayed, but did not prevent melting at the core centerline. Effect on temperatures at the core periphery was, however, pronounced. Cladding temperatures at the peripheral fuel bundles reached only 1600 K, until quenched by restoring of the B-loop AFW (at 153 minutes). A significant result is that restoration of the B-loop AFW cooled the reactor core in case of 5 kg/s injection. This occurred because not enough hydrogen was produced to block the steam generator.

Variation of the timing of HPI resulted in two distinct end state categories. Restoring of the B-loop AFW at 153 minutes terminated the accident propagation in cases characterized by high HPI rate at 100-137 minutes and consequent low hydrogen production. Cases with low HPI rates after 100 minutes resembled the base case, resulting in highly degraded core by the end of phase 2. This result could not have been predicted by the calculated core water levels only. The
minimum water levels in cases 4 and 5 differed by about 0.1 m, still the core states were totally different.

III. Significance of the results

The main findings of the MAAP 3.0 calculations in the base case were:

- MAAP overpredicted the primary system pressure. This is partly due to the incorrect steam superheat.
- Pressurizer water inventory was dumped into the primary system late in phase 2 (160 minutes).
- Core uncovering began at 106 minutes. The calculated minimum liquid level was 3.4 m above the bottom of the RV head (0.95 m above the bottom of the fuel rods), at 160 minutes.
- Fuel melting occurred above the core midplane in all radial regions. Molten material relocated to regions located 0.5 - 1 m from the bottom of the fuel rods.
- Calculated hydrogen release was 240 kg, at the end of phase 2, which is close to the value estimated from the plant data.
- 75 % of the noble gases and CsI was released from the fuel. Negligible amount of fission products was released to the containment by the end of phase 2.

The main observations of the parametric runs were:

- Increasing HPI from 4 kg/s to 5 kg/s did not entirely prevent fuel melting.
- Timing of HPI was important in determining the core status at the end of phase 2. No melting occurred in cases characterized by high injection rates at the beginning of core uncovering.
- Timing of the hydrogen production and subsequent hydrogen blocking of the steam generators is of major importance in simulating of the TMI-2 accident. Restoring AFW to the B-loop at 153 minutes terminated the accident in cases of low hydrogen production.

IV. Conclusions
The TMI-2 analysis involves so many unknown boundary conditions that computer codes can not be meaningfully validated against it. The merit of the exercise lies in the fact that it was, after all, a real accident occurring in a real nuclear power plant, and hence the completeness of the code modeling can be tested. All relevant components, phenomena and actions of phases 1 and 2 could be modeled by the MAAP 3.0 code. This was not a surprise, because experiences from the earlier MAAP 2.0 TMI-2 calculations had been taken into account in MAAP 3.0 development. The parametric runs provided the most significant results. It was demonstrated, that minor changes in the assumed HPI rate history in phase 2 resulted in completely different end states by the end of phase 2. A key phenomenon was the rate and timing of the hydrogen production and subsequent hydrogen blocking of the steam generators.

V. References


Table 1. Calculated sequence of events in the base case.

<table>
<thead>
<tr>
<th>TIME (s)</th>
<th>EVENT</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>MSIV closed</td>
</tr>
<tr>
<td>0.0</td>
<td>Accumulator block valve closed</td>
</tr>
<tr>
<td>0.0</td>
<td>LPI forced off</td>
</tr>
<tr>
<td>0.0</td>
<td>AUX feed water forced off</td>
</tr>
<tr>
<td>0.0</td>
<td>Main FW shut off</td>
</tr>
<tr>
<td>0.0</td>
<td>S/G MSIV forced closed</td>
</tr>
<tr>
<td>1.7</td>
<td>Reactor scram</td>
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<td>1.7</td>
<td>PZR PORV stuck open</td>
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Table 2. Core temperatures at the end of phase 2 in the base case.

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Table 3. Core node masses at the end of phase 2 in the base case.

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<th>Mass (kg)</th>
<th>end state/initial state</th>
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Table 4. Variation of HPI in the parametric runs.

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<td>0</td>
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<td>11</td>
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</table>
Fig. 1. Nodalization of the B&W primary system in the MAAP 3.0 code.

Fig. 2. Calculated and measured primary system pressure in the base case.
Fig. 3. Calculated and measured pressurizer water level in the base case.

Fig. 4. Core liquid level measured from the bottom of the RPV in the base case.
Fig. 5. Cladding temperatures at the core center in the base case.

Fig. 6. Mass of hydrogen generated in the base case.
MAAP 3.0 CALCULATIONS FOR THE PHASES 1 AND 2

R. Sairanen
Technical Research Centre of Finland
Nuclear Engineering Laboratory
I. INTRODUCTION

The first 174 minutes (phases 1 and 2) of the TMI-2 accident were calculated by using the Modular Accident Analysis Program (MAAP), Version 3.0 at the Technical Research Centre of Finland. A sequence of MAAP codes have been developed by Fauske and Associates, Inc. (FAI), of which the version 3.0 was released in 1986. MAAP is an integral code that includes models for all the important phenomena which might occur within the plant in a severe accident. One of the goals of the program has been fast execution time, consequently quite simple modeling is used for some of the phenomena.

MAAP 2.0 input described in the EPRI report (1) was utilized as a basis for construction of the MAAP 3.0 input, augmented with information from the preliminary MAAP 3.0 B calculations (2). Our MAAP 3.0 input was later commented by A. Sharon from FAI, and two significant errors was pointed out:
- steam superheat was too low,
- pressurizer was attached erroneously to the B-loop due to the misleading MAAP 3.0 input instructions.

MAAP 3.0 B had replaced MAAP 3.0 at VTT at the time of receiving the FAI's comments, and hence the MAAP 3.0 calculations were not remade. Effects of the incorrect pressurizer location was later studied by the MAAP 3.0 B code. The error did indeed affect the calculation from 73 minutes to 100 minutes when the system was highly unsymmetric, but the effect on heatup phase was surprisingly small. Too low steam superheat probably caused overprediction of the primary system pressure in the beginning of the accident. All results described hereupon refer to the original (uncorrected) MAAP 3.0 calculations.

II. Results

A. Node Diagram

MAAP assumes a fixed nodalization of the primary system. A schematic of the primary system nodalization model for B&W plants is shown in Fig. 1. The code has separate models for the so-called "broken" (=B) and "unbroken" (=A) loop. The number of control volumes is small compared to detailed thermal-hydraulic codes.
MAAP thermal-hydraulic nodalization is further simplified by the convention that water pools are separately accounted for in the cold legs, intermediate legs and downcomer only. The horizontal parts of the cold legs and water in the lower head are included in the downcomer water pool. The nodalization shown in Fig. 1 is used as such in gas transport calculations.

B. Discussion of Results

The base case

A summary of the calculated sequence of events is presented in Table 1. The calculated primary system pressure is compared with the measurements in Fig. 2. A common feature of all the VTT'S MAAP 3.0 calculations was an overprediction of the primary system pressure by about 100 psi (0.7 MPa) partly due to the incorrect steam superheat. The pressure drop at 160 minutes was caused by dumping of the pressurizer water into the primary system.

The calculated pressurizer water levels shown in Fig. 3 resembled the MAAP 2.0 results (1). A sequence of drops was seen on the calculated water level. The first drop occurred at 73 minutes after idling the B-loop, the second after restoration of the AFW to the A-loop (99 min) and subsequent primary system cooling. The third drop at 160 minutes resulted in dumping of all pressurizer water into the primary system.

Fig. 4 shows the calculated core water level in the base case. Core uncoverage was calculated to begin at 106 minutes, when water level decreased below 6.12 m (measured from the bottom of the RV head). The calculated minimum level was 3.4 m above the bottom of the RV head (0.95 m above the bottom of the fuel rods), predicted to occur at 160 minutes.

The calculated cladding temperatures at the core center are shown in Fig. 5. Heatup of the uppermost nodes began after core uncoverage at 106 minutes and rapid escalation of the temperatures began at 140-150 minutes. The outer radial nodes did exhibit similar characteristics, except that fuel melting began about 10 minutes later. All nodes above the core midplane reached the assumed eutectic melt temperature, 2500 K, resulting in major relocation of core materials into the core.
bottom nodes (nodes 2 and 3). The calculated core temperatures and material
distribution at 174 minutes is presented in Tables 2 and 3.

An integrated release of about 240 kg of hydrogen was estimated by the end of
phase 2. Most of the hydrogen was produced during the relatively short interval
between 140 and 155 minutes, as shown in Fig 6. Hydrogen production was
terminated by the blockage of the core at 155 minutes, after which steam could not
enter the hot core regions.

Timing of the fission product release resembled hydrogen production. A release of
about 75 % of the noble gases, CsI and CsOH was calculated by the end of phase
2, whereas release of other fission products was small. Pressurizer PORV was
closed at 142 minutes, 1-2 minutes after major fission product release from fuel
was calculated to begin. Hence, only a small fraction (2*10-5 of CsI inventory, for
example) was transported from the primary system to the containment.

Parameter variations

Seven sensitivity runs were calculated by varying the rate and timing of the HPI as
recommended in EGG-TMI-7833 (3). The HPI rates used after 100 minutes in the
sensitivity runs are shown in Table 4.

Increase of the HPI rate after 100 minutes from 4 to 5 kg/s delayed, but did not
prevent melting at the core centreline. Effect on temperatures at the core periphery
was, however, pronounced. Cladding temperatures at the peripheral fuel bundles
reached only 1600 K, until quenched by restoring of the B-loop AFW (at 153
minutes). A significant result is that restoration of the B-loop AFW cooled the
reactor core in case of 5 kg/s injection. This occurred because not enough hydrogen
was produced to block the steam generator.

Variation of the timing of HPI resulted in two distinct end state categories.
Restoring of the B-loop AFW at 153 minutes terminated the accident propagation
in cases characterized by high HPI rate at 100-137 minutes and consequent low
hydrogen production. Cases with low HPI rates after 100 minutes resembled the
base case, resulting in highly degraded core by the end of phase 2. This result
could not have been predicted by the calculated core water levels only. The
minimum water levels in cases 4 and 5 differed by about 0.1 m, still the core states were totally different.

III. Significance of the results

The main findings of the MAAP 3.0 calculations in the base case were:

- **MAAP overpredicted the primary system pressure.** This is partly due to the incorrect steam superheat.
- **Pressurizer water inventory was dumped into the primary system late in phase 2 (160 minutes).**
- **Core uncovering began at 106 minutes.** The calculated minimum liquid level was 3.4 m above the bottom of the RV head (0.95 m above the bottom of the fuel rods), at 160 minutes.
- **Fuel melting occurred above the core midplane in all radial regions.** Molten material relocated to regions located 0.5 - 1 m from the bottom of the fuel rods.
- **Calculated hydrogen release was 240 kg, at the end of phase 2, which is close to the value estimated from the plant data.**
- **75 % of the noble gases and CsI was released from the fuel.** Negligible amount of fission products was released to the containment by the end of phase 2.

The main observations of the parametric runs were:

- **Increasing HPI from 4 kg/s to 5 kg/s did not entirely prevent fuel melting.**
- **Timing of HPI was important in determining the core status at the end of phase 2.** No melting occurred in cases characterized by high injection rates at the beginning of core uncovering.
- **Timing of the hydrogen production and subsequent hydrogen blocking of the steam generators is of major importance in simulating of the TMI-2 accident.** Restoring AFW to the B-loop at 153 minutes terminated the accident in cases of low hydrogen production.

IV. Conclusions
The TMI-2 analysis involves so many unknown boundary conditions that computer codes can not be meaningfully validated against it. The merit of the exercise lies in the fact that it was, after all, a real accident occurring in a real nuclear power plant, and hence the completeness of the code modeling can be tested. All relevant components, phenomena and actions of phases 1 and 2 could be modeled by the MAAP 3.0 code. This was not a surprise, because experiences from the earlier MAAP 2.0 TMI-2 calculations had been taken into account in MAAP 3.0 development. The parametric runs provided the most significant results. It was demonstrated, that minor changes in the assumed HPI rate history in phase 2 resulted in completely different end states by the end of phase 2. A key phenomenon was the rate and timing of the hydrogen production and subsequent hydrogen blocking of the steam generators.

V. References


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<table>
<thead>
<tr>
<th>TIME (s)</th>
<th>EVENT</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>MSIV closed</td>
</tr>
<tr>
<td>0.0</td>
<td>Accumulator block valve closed</td>
</tr>
<tr>
<td>0.0</td>
<td>LPI forced off</td>
</tr>
<tr>
<td>0.0</td>
<td>AUX feed water forced off</td>
</tr>
<tr>
<td>0.0</td>
<td>Main FW shut off</td>
</tr>
<tr>
<td>0.0</td>
<td>S/G MSIV forced closed</td>
</tr>
<tr>
<td>1.7</td>
<td>Reactor scram</td>
</tr>
<tr>
<td>1.7</td>
<td>PZR PORV stuck open</td>
</tr>
<tr>
<td>42.6</td>
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<td>Letdown flow off</td>
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<td>B-loop S/G dry</td>
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<td>A-loop S/G dry</td>
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<td>270.3</td>
<td>Quench tank rupture disk failed</td>
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<td>500.1</td>
<td>AUX feedwater on</td>
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<td>B-loop S/G not dry</td>
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Table 2. Core temperatures at the end of phase 2 in the base case.

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Table 3. Core node masses at the end of phase 2 in the base case.

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Table 4. Variation of HPI in the parametric runs.

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</tbody>
</table>
Fig. 1. Nodalization of the B&W primary system in the MAAP 3.0 code.

Fig. 2. Calculated and measured primary system pressure in the base case.
Fig. 3. Calculated and measured pressurizer water level in the base case.

Fig. 4. Core liquid level measured from the bottom of the RPV in the base case.
Fig. 5. Cladding temperatures at the core center in the base case.

Fig. 6. Mass of hydrogen generated in the base case.