

Special Section on TMI-2 Vessel Investigation Project

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Three Mile Island—New Findings 15 Years After the Accident

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Abstract: *On March 28, 1979, the Three Mile Island Unit 2 (TMI-2) nuclear power plant underwent a prolonged small-break loss-of-coolant accident, compounded by human errors and equipment failures, that resulted in severe damage to the reactor core. The accident, the most severe that has occurred in a commercial pressurized-water reactor, resulted in a partial melting of the reactor core and significant release of fission products from the fuel into the reactor vessel and the containment building. The progression of the TMI-2 accident was mitigated by the injection of emergency cooling water.*

A great deal has been learned about the TMI-2 accident since it occurred 15 years ago. Much of our knowledge about the accident has evolved over time as cleanup, defueling, examinations inside the reactor vessel, and analyses have been completed. In October 1993 a 5-year major research project on the damaged reactor, called the TMI-2 Vessel Investigation Project (VIP), was completed. This article summarizes the views of the accident over the past 15 years, what we have learned from the VIP, and the broad significance of these findings. In particular, the VIP has added significant insights about the TMI-2 accident in the areas of reactor vessel integrity and issues related to accident management.

By the time the Kemeny Commission released its report to President Carter in October 1979 the circumstances that led to the accident, the course of events, and the actions taken by plant operators were clear for the plant

systems for which measurements and records were available: these were the systems outside containment and inside to a lesser extent. As an observer attempted to focus attention on the reactor coolant system and the reactor vessel, clarity vanished, and he or she could only attempt to speculate on events and final conditions by inferring from external measurements and judgment. An article published in the *Spectrum* of the Institute of Electrical and Electronics Engineers (IEEE) gives an excellent account of the widely held view in the months after the accident: "... This was because most of the core damage was to the cladding, which primarily yields noble gases. Iodine is released by damage to the fuel pellets, and this damage was minimal at Three Mile Island."¹

The article identified the 100-minute mark after the main feedwater pumps tripped, which was the start of the accident, as the point of time before which there was the possibility of recovery to prevent a severe accident and after which core damage was unavoidable. Notice especially, too, the statement that most of the damage was to the clad, and the fuel pellets themselves experienced minimal damage. Four years passed before the error of this latter view came to light. This change in view is marked in a second *Spectrum* article: "What is now known is that most of the 177 fuel assemblies ... were nearly completely destroyed in the upper quarter of the reactor core. What exists now is a void measuring 9.3 cubic meters. ... Other material from the core void is believed to be at the bottom of the reactor vessel."² The suggestion that "resolidified mass from the molten

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Table 3 Reactor Shutdowns by Reactor Type and Reactor Age^a
(Period Covered is the First Half of 1994)

Years in commercial operation (C.O.)	BWRs (37)						PWRs (76)					
	Exposure during the period (in reactor years)	Number		Shutdown rate (annualized for the period)	Cumulative number	Cumulative shutdown rate per reactor year	Exposure during the period (in reactor years)	Number		Shutdown rate (annualized for the period)	Cumulative number	Cumulative shutdown rate per reactor year
		Reactors	Shutdowns					Reactors	Shutdowns			
Not in C.O. ^b	0.496	1	0	0.00	330	22.79	0.000	0	0	0.00	336	34.24
First year of C.O.	0.000	0	0	0.00	121	9.00	0.496	1	2	4.04	280	9.96
Second through fourth year of C.O.	0.019	1	0	0.00	264	6.29	0.991	2	2	2.02	526	5.59
Fifth through seventh year of C.O.	2.459	5	2	0.81	178	4.40	4.709	10	7	1.49	317	3.27
Eighth through tenth year of C.O.	3.469	7	6	1.73	201	5.31	6.105	15	5	0.82	367	3.73
Eleventh through thirteenth year of C.O.	0.991	2	1	1.01	271	5.74	4.548	10	3	0.66	496	4.26
Fourteenth through sixteenth year of C.O.	0.496	1	1	2.02	396	6.20	2.409	5	2	0.83	364	3.24
Seventeenth through nineteenth year of C.O.	2.724	7	1	0.37	280	4.65	5.662	13	12	2.12	252	2.66
Twentieth through twenty-second year of C.O.	4.709	10	9	1.91	154	4.63	9.273	20	9	0.97	94	2.10
Twenty-third through twenty-fifth year of C.O.	2.973	6	7	2.35	47	3.57	1.982	4	5	2.52	28	2.02
Twenty-sixth through twenty-eighth year of C.O.	0.000	0	0	0.00	8	2.67	0.991	2	2	2.02	16	2.67
Twenty-ninth through thirty-first year of C.O.	0.238	1	1	4.20	9	3.00	0.000	0	0	0.00	5	1.67
Thirty-second through ninety-ninth year of C.O.	0.257	1	0	0.00	0	0.00	0.496	1	0	0.00	0	0.00
Total	18.831		28	1.49	2259	6.07	37.662		49	1.30	3081	4.28

^aAge is defined to be the time (in years) from the start of commercial operation to the time of the shutdown event, except for the first line, which lists reactors not yet in commercial service (see b below).

^bThis category includes reactors licensed for full-power operation but not yet commercial. During this reporting period reactors in this category included 1 BWR (Shoreham) and no PWRs.

material could exist below the cavity in the core" represents a drastic change in the view of the accident in comparison with the October 1979 IEEE *Spectrum* article.

By 1987 the Three Mile Island (TMI) research had advanced considerably, and the investigators had developed a much better understanding of the accident sequence on the basis of the location and condition of core materials, fragments, and once-molten core materials that had resolidified. On the basis of this research, knowledge of the end-state condition of the TMI-2 reactor vessel and core is shown in Fig. 1. A central cavity existed in the upper portion of the core approximately 1.5 m above a loose debris bed. A previously molten region that was contained by partly or fully metallic crust layers was found below the loose debris

layer. Overall, at least 45% (62 metric tons) of the core had melted. Video examinations also indicated that approximately 19 000 kg (19 metric tons) of molten material had relocated onto the lower head of the reactor vessel.

Information presented in a paper entitled "A Scenario of the Three Mile Island Unit 2 Accident"³ describes the accident in seven periods: (1) the first 100 minutes of the loss-of-coolant accident, (2) initial core heat-up, (3) formation of the upper core debris bed, (4) growth of a pool of molten core material, (5) injection of emergency core coolant system water, (6) failure of the crust supporting the molten pool and flow of molten material to the bottom of the vessel, and (7) finally quenching and cooling of the lower debris bed and eventual stabilization of conditions.

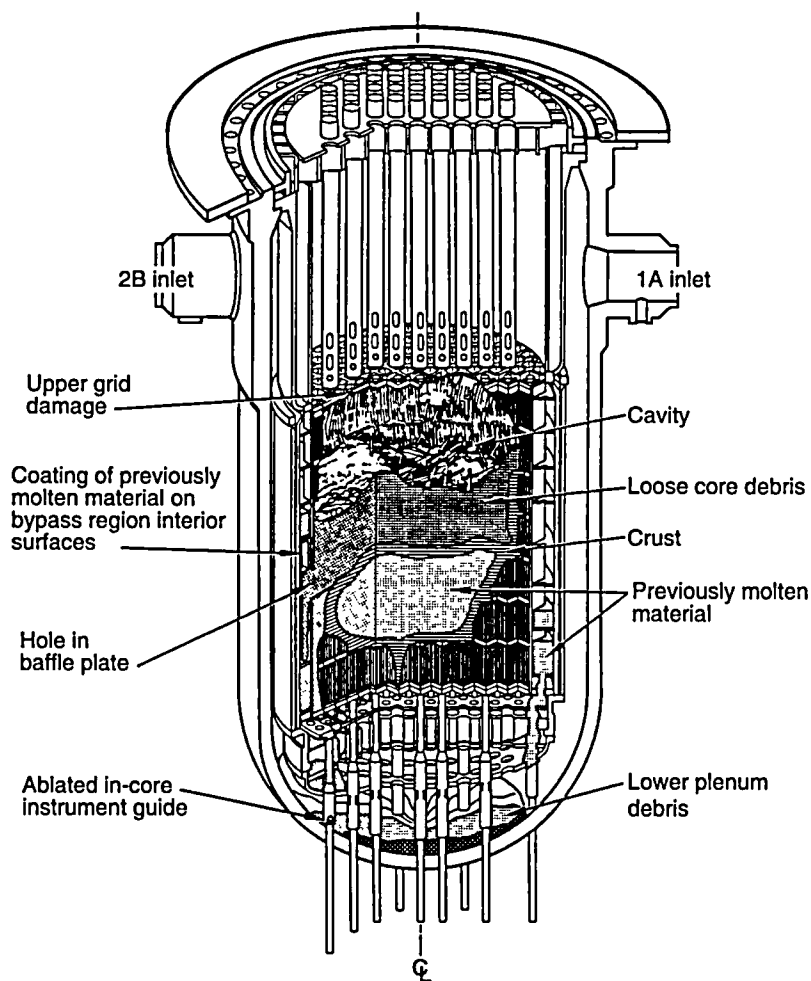


Fig. 1 TMI-2 reactor vessel end-state configuration.

The change indicated in the 1987–1989 views, compared with the views of 1984, is in the condition of the vessel, with the suggestion of “possible thermal ablation of the reactor vessel lower head.” At the same time, the scenario confirms the view of the first 100 minutes of the accident that was presented in the 1979 *Spectrum* article. So the 1979 view of the first 100 minutes has stood the test of time, whereas the view of what subsequently took place within the vessel has changed drastically.

It is interesting to reflect on the long time (i.e., 8 to 10 years) that it took to develop the final view of the TMI core conditions. Did the initial erroneous view extend the time required to obtain the facts? Probably not. The long lead time required to develop the means of discovery and solve myriad technical problems associated with the removal of reactor internals, core, and fuel debris under difficult working conditions played the major role in extending the effort.

INITIATION OF THE TMI-2 VESSEL INVESTIGATION PROJECT

As researchers gained more information in the early and mid-1980s concerning the extent of damage to the TMI-2 reactor, they realized that cleanup of the reactor would take several years and would require the cooperation of both private industry and government agencies. As a result, an organization named GEND, which included General Public Utilities Nuclear Corporation (GPUN), the Electric Power Research Institute (EPRI), the U.S. Nuclear Regulatory Commission (NRC), and the U.S. Department of Energy (DOE), was formed. GEND gave technical and financial assistance to the owner of the TMI-2 reactor, GPUN was responsible for ongoing plant cleanup operations, and DOE was responsible for providing transportation and interim storage of the core until permanent disposition was decided. DOE also supported an extensive research program, the TMI-2 Accident Evaluation Program (AEP), to develop a consistent understanding of the accident. The primary objective of the DOE AEP was to develop an understanding of (1) core damage progression in the upper core region, (2) the heat-up and the formation and growth of the molten central region of the core, (3) the relocation of approximately 19 metric tons of debris to the lower head, and (4) the release of fission products to the reactor vessel and the containment.

The AEP was focused primarily on core damage progression and the mechanisms that controlled fission-

product behavior. Observations made during the latter portions of the defueling effort, however, indicated that the accident progressed even further than was envisioned when the AEP was established. Molten core materials were found to have moved laterally through the east-side core baffle and former plates and into the core bypass region between the core-former wall and the core barrel. Visual observation also indicated the presence of a large hole approximately 0.6 m wide and 1.5 m high extending across the lower portion of three core-former plates. The 1.9-cm-thick core-former plates and sections of three 3.2-cm-thick horizontal baffle plates were melted in this region. Molten material from the core region flowed through this hole and into the upper core support assembly. Loose debris was found in the area behind the baffle plates and extended completely around the core region. It was estimated that 4200 kg of core debris was in the upper core support region. Closed-circuit television pictures indicated evidence of thermal damage to instrument structures in the lower plenum and around flow holes in the elliptical flow distributor.

The principal conclusions from the DOE program were that the TMI-2 core damage progression involved the formation of a large consolidated mass of core material surrounded by supporting crusts, the failure of the supporting crusts, and finally, the long-term cooling of a large volume of molten core material. The TMI-2 accident demonstrated that, at least for one severe accident scenario, the accident can be terminated and confined to the reactor pressure vessel by cooling water before the lower head fails. However, there was no quantitative information that could be used to determine how close the vessel was to failure.

In October 1987 the NRC proposed that a joint international cooperative program be formed that would be sponsored by the Nuclear Energy Agency of the Organization for Economic Cooperation and Development (NEA-OECD). This program would conduct further investigations of potential damage to the TMI-2 reactor vessel lower head from the relocation of molten fuel to that region. A steering committee was established to determine if there were sufficient interest from the OECD member countries to warrant formation of such a program. The OECD efforts led to issuing the “Agreement to Investigate the Three Mile Island-2 Reactor Pressure Vessel” in June 1988. Signatories to the project, commonly called the Vessel Investigation Project (VIP), included Belgium, Finland, France, Germany, Italy, Japan, Spain, Sweden, Switzerland, the United Kingdom, and the United States.

As described in the formal project agreement, the objectives of the VIP were to do the following: Jointly carry out a study to evaluate the potential modes of failure and the margin to failure of the TMI-2 reactor vessel during the TMI-2 accident. The conditions and properties of material extracted from the lower head of the TMI-2 pressure vessel will be investigated to determine the extent of damage to the lower head by chemical and thermal attack, the thermal input to the vessel, and the margin of structural integrity that remained during the accident.⁴

The examinations performed under the VIP went beyond the work that had been performed during the previous TMI-2 examinations. Specifically, the VIP plan was to obtain and examine samples of the lower-head steel, instrument penetrations, and previously molten debris that was attached to the lower head and use this information to estimate the vessel margin to failure. The schedule for the VIP was determined by the tasks required for fuel removal, the development of the cutting tools to remove lower-head samples, the laboratory metallurgical work, and finally the study and analyses of results. It took nearly 5 years to carry out the project, during which time nearly all the objectives were accomplished.

PROJECT ORGANIZATION

The management and organization of the VIP were defined in the 1988 formal agreement that established the project. Overall control and direction of the VIP were vested in a Management Board that consisted of one member designated by each of the signatories. The primary function of the Management Board was to approve the overall VIP work scope and budget, including the allocation of tasks among the signatories.

A Program Review Group was also formed that consisted of one member designated by each signatory. The primary function of the Program Review Group was to act as the technical advisor to the Management Board for both ongoing activities and future work. The Program Review Group was also chartered to provide technical advice and recommendations to the VIP operating agent, NRC, which was responsible for implementing project objectives in accordance with the project agreement and directions from the Management Board.

MAJOR PROJECT ELEMENTS

The VIP objectives were realized through a combination of several major activities that included extraction of

vessel steel, nozzle, and guide tube samples from the lower-head region; examinations of the extracted material; and analyses to determine the structural integrity that remained in the vessel. Various project members examined the steel samples, along with the nozzles, guide tubes, and previously molten debris that were found in the lower-head region to determine the condition and properties of the samples and the extent of damage to the lower head during the accident. The results of these examinations were used to assist in quantifying potential reactor vessel failure modes, to estimate the vessel steel temperatures in the lower head during the accident, and to develop physical and mechanical property data to support the analysis effort. In the area of analysis, scoping calculations and sensitivity studies were performed in an effort to quantify the margin to failure for different reactor failure modes and to identify which modes had the smallest margin to failure during the accident.

The significant conclusions and accomplishments of each of the major project elements are discussed in the following text. Additional details on each of the major VIP elements and project results and conclusions are provided in a series of reports that were issued under the VIP.⁵⁻¹²

SAMPLE ACQUISITION

One of the major accomplishments of the VIP, accounting for approximately one-half of the total cost of \$9 million, was the recovery of samples from the TMI-2 vessel lower head. This task, which was performed under the direction of MPR Associates, Inc., required careful planning because only a 30-day window was available at the site to set up the equipment and remove the samples. Specialized extraction tools had to be developed and tested before the actual sample removal.

One of the unique challenges in removing the samples was that the reactor vessel could not be breached or significantly weakened. Also, work had to be performed on a shielded platform mounted 40 feet above the lower head while samples that were covered by highly borated water were extracted. Because this was a first-of-a-kind process and the available time was limited, the exact number of samples removed could not be predicted in advance. It was hoped that 8 to 20 samples could be obtained. Despite extensive mock-up testing of the cutting tools, which used an electrical discharge metal disintegration process for cutting, a number of unexpected problems arose during the first half of the time for

working in the reactor vessel, and no samples were taken during that time. The effort was very successful in the last half of the window, however, and 15 vessel steel samples, 14 nozzles, and 2 guide tubes were removed from the vessel in February 1990. The location of these samples is shown in Fig. 2. The prism-shaped vessel steel samples extended approximately half way through the 13.7-cm-thick reactor vessel wall.

GPU Nuclear provided access to the reactor during this window at its cost, and the VIP paid only the incremental cost of sample cutting and removal. An extension of the 30-day window would have added greatly to the cost of the project and was not financially possible for the VIP.

VESSEL STEEL EXAMINATIONS

Argonne National Laboratory (ANL) in the United States coordinated the metallographic examinations and mechanical property tests of the vessel steel samples. All the lower-head steel samples were visually examined, decontaminated, sectioned, and sent to eight of the VIP member countries for testing. The participants that examined the vessel steel samples were Belgium, Italy, Finland, France, Germany, Spain, the United Kingdom,

and, in the United States, ANL and Idaho National Engineering Laboratory (INEL). Examinations performed by the project participants included tensile, creep, and Charpy V-notch impact tests, microhardness measurements, micro and macro photography, and chemical composition. The primary purpose of these tests was to determine the mechanical properties of the lower-head steels over the temperature range experienced during the accident. Optical metallography and hardness tests were performed to evaluate the microstructure to estimate the maximum temperature of various portions of the lower head reached during the accident.

The results of the wide range of inspections, mechanical property determinations, and metallographic examinations of the lower-head vessel samples revealed several important and previously unknown facts relating to the degree of thermal attack on the lower head. Overall, these examinations revealed that a localized hot spot formed in an elliptical region on the lower head that was approximately 1 m by 0.8 m, as shown in Fig. 3. The hot spot was in the area where visual observations made during the defueling process indicated that the most severe nozzle damage had occurred. Metallographic examinations of samples taken from this region indicated that the inner surface of the vessel steel reached temperatures between 1075 and 1100 °C during the accident. At this

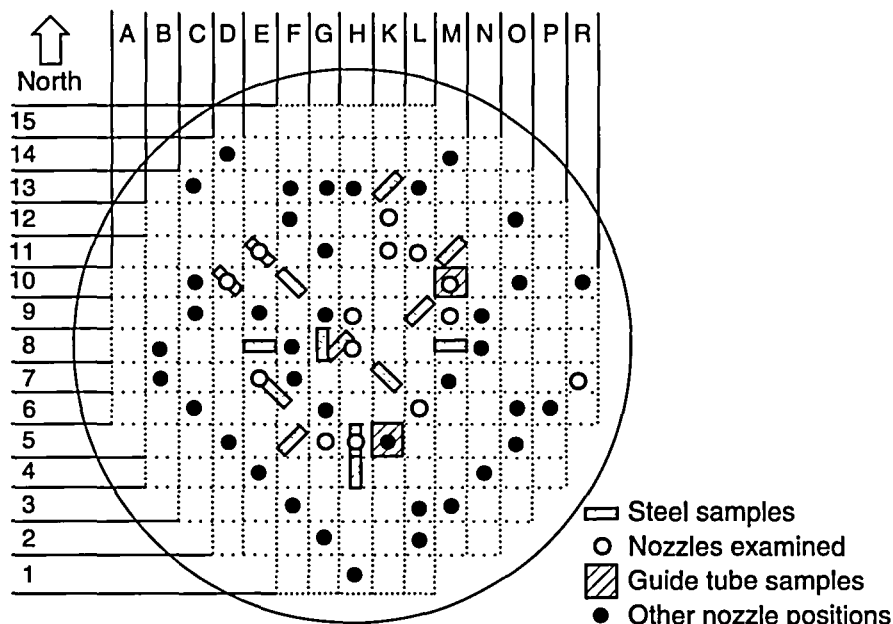


Fig. 2 Location of lower-head steel, nozzle, and guide tube samples.

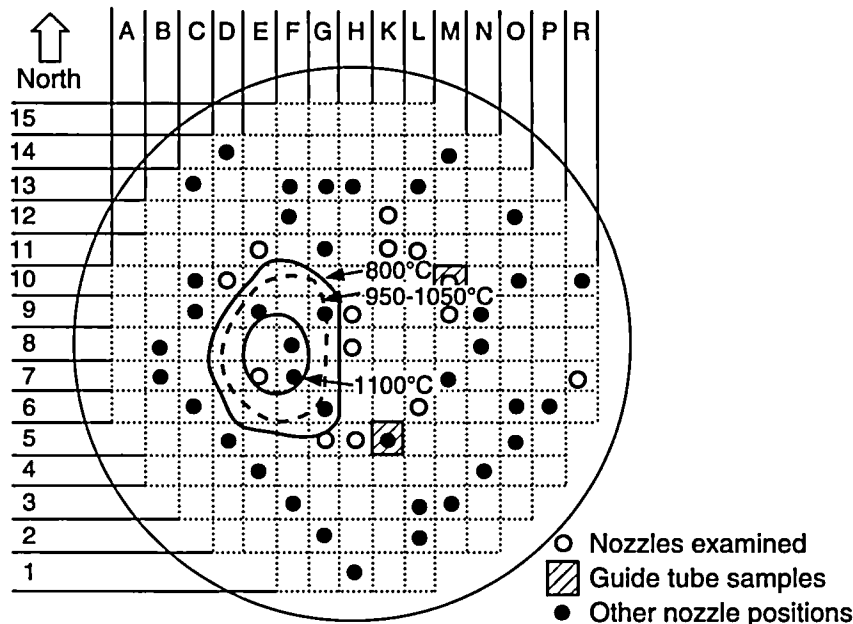


Fig. 3 Lower-head hot spot location.

location, temperatures 0.45 cm into the vessel wall were estimated to be $100 \pm 50^\circ\text{C}$ lower than the peak vessel inner surface temperature.

By comparing results of the TMI-2 lower-head sample examinations with results from metallurgical examinations of heat-treated samples from an equivalent ("archive") steel from the Midland reactor, the vessel steel temperatures, time at temperature, and cooling rate were estimated. Standards with known thermal histories were prepared from the Midland archive material and later from actual as-fabricated TMI-2 material. The standards provided a means for comparing a similar material with a known thermal history to TMI-2 material with an unknown thermal history. As the standards were prepared and examined, various metallurgical observations revealed a stepwise process that could be used in determining thermal histories of the TMI-2 samples. G. Korth⁶ constructed a diagram (shown in Fig. 4) that illustrates the metallurgical changes with time and temperature of the Midland and TMI-2 lower head A 533 B steel with a 308L stainless weld clad. Because the vessel was stress-relieved at 607°C after the weld clad was added, no thermal effects from the accident could be detected at or below this temperature, and therefore the diagram shows only metallurgical observations for temperatures above this point. The lowest temperature indicator, above the

stress relief temperature, was the ferrite-austenite transformation, which starts at 727°C and is complete by about 830°C . Variations in the typical as-fabricated hardness profile were evident when this temperature threshold was exceeded. The next indicator is the dissolution or dissipation of a dark feathery band at the interface between the base metal and the stainless steel clad; this occurs between 800 and 925°C , depending on the time. The next indicator of increasing temperature is the appearance of small equiaxed grains, which formed in the A 533 B steel adjacent to the interface at temperatures between 850 and 900°C and disappeared between 1025 and 1100°C as they were consumed by grain growth in the low-alloy steel. Grain growth in the A 533 B steel becomes significant above approximately 950 to 1075°C , depending on the time involved. The highest temperature indicator shown on the diagram is the change in morphology of the δ -ferrite islands in the stainless steel cladding. In the approximate range of 975 to 1000°C at 100 minutes or 1100 to 1125°C at 10 minutes, the δ -ferrite islands begin to lose their slender branch-like morphology and become spherical. Additional details on how these indicators were used to estimate the TMI-2 vessel steel sample temperatures are provided in Ref. 6.

Temperatures in the hot spot were considerably higher than those in the surrounding region of the lower head.

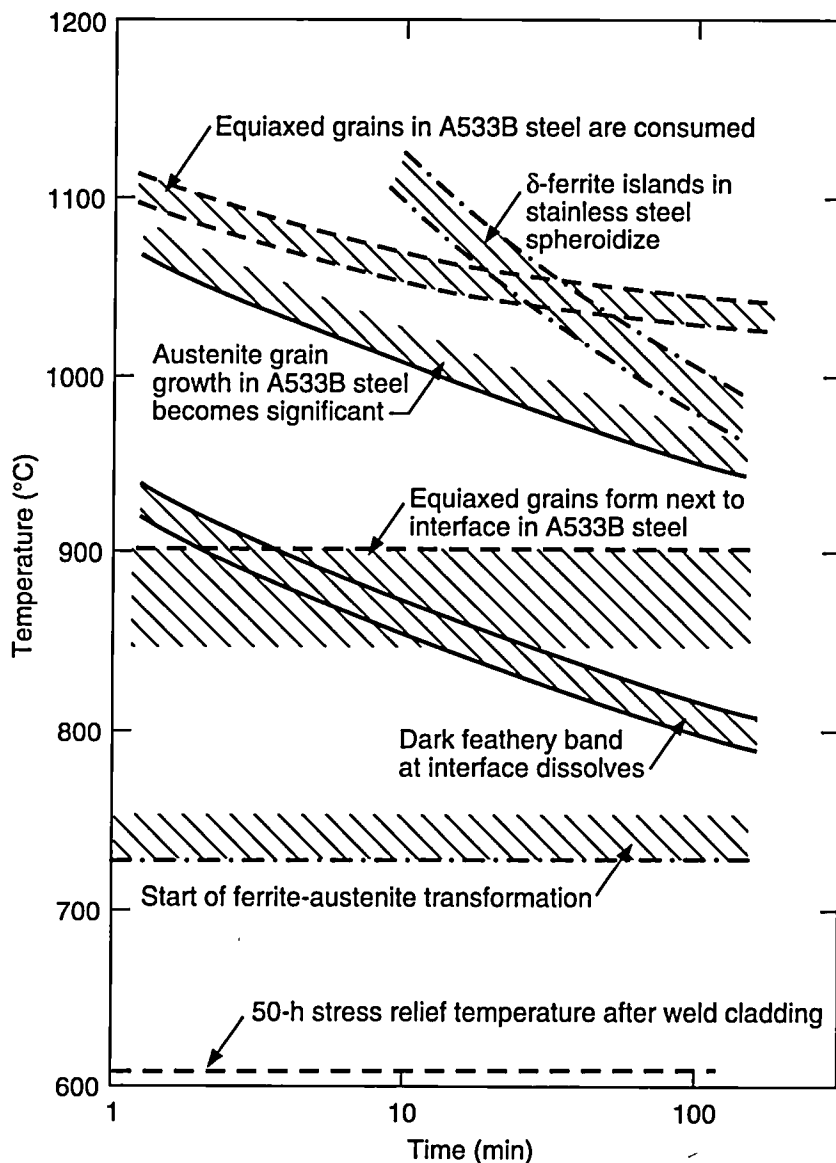


Fig. 4 Diagram of time-temperature observations of A 533 B pressure vessel steel clad with type 308L stainless steel.

Generally, the vessel temperature away from the hot spot did not exceed the 727 °C ferrite-austenite transformation temperature for the A 533 B pressure vessel steel. The results of metallographic and hardness examinations could determine whether the 727 °C transition temperature in the steel was exceeded. However, because microstructural and associated hardness changes in the steel do not occur below 727 °C, it was not possible to estimate how far below 727 °C the vessel steel temperature was

away from the hot spot. Therefore there is a large uncertainty in the actual vessel steel temperature away from the hot spot. The temperature of the vessel inner surface in this region during the accident could have ranged from a minimum of 327 °C (normal plant operating conditions) to a maximum of 727 °C.

The hardness profiles of most of the TMI-2 samples had the typical characteristic profile of as-fabricated material, as shown in the shaded band in Fig. 5; but

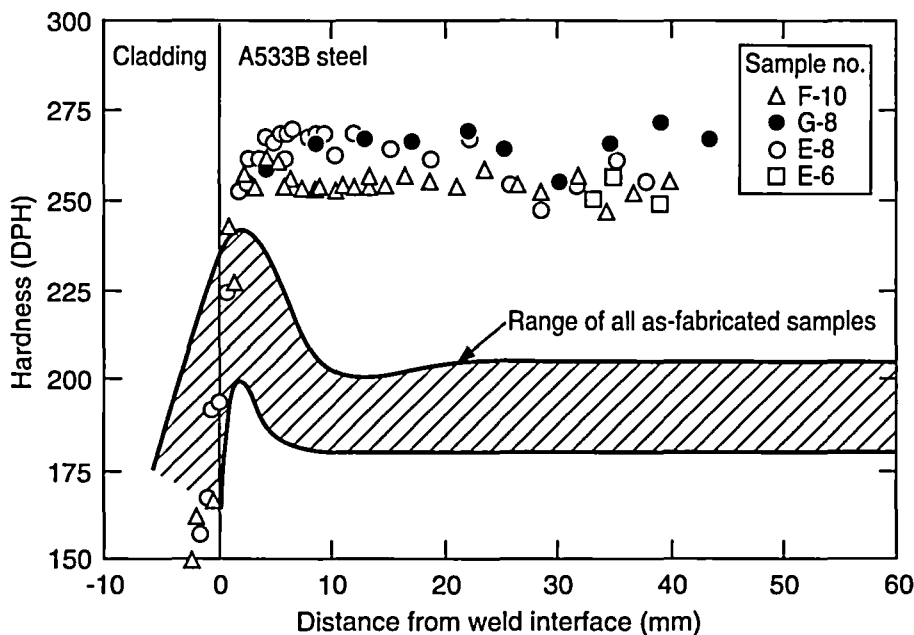


Fig. 5 Hardness profiles of samples F-10, G-8, E-8, and E-6 compared to the as-fabricated samples.

the hardness profiles from sample locations E-6, E-8, F-10, and G-8 (see Fig. 2) were markedly different from all other samples, as shown in this figure. In these four samples the characteristic hardness profile through the heat-affected zone near the clad weld interface had risen sharply to much higher levels and was then sustained throughout the full sample depth. Heat-affected bands from the weld cladding were not evident in these four samples but were completely eliminated by the thermal effects of the accident. Two other samples (H-8 and F-5) also showed anomalies in the hardness profiles. Results of these hardness profile measurements indicated which samples exceeded the 727 °C transformation temperature.

The steel examinations were also able to provide data on the cooling rate of the lower-head hot spot. Microstructural and hardness observations in the as-received state for two samples in the hot spot reflected the austenitizing heat treatment and the subsequent relatively rapid cooling of this material during the accident. Cooling rates were estimated to have been in the range of 10 to 100 °C/min through the transformation temperature. It was also determined that samples in the hot spot may have remained at their peak temperature for as long as 30 minutes before being cooled.

Mechanical property tests performed on the TMI-2 vessel steel samples produced a wealth of high-temperature mechanical property data. Results of these

tests, along with observations of the samples, provided information on the postaccident condition of the lower head as well as input to the margin-to-failure analysis. Creep tests performed at 600 to 700 °C indicated no significant differences in behavior between samples that exceeded a maximum temperature of 727 °C and those which did not. Tensile tests for specimens that exceeded 727 °C showed significantly higher strengths at room temperature and at 600 °C when compared with those which did not exceed 727 °C. The tensile tests at lower test temperatures further confirmed the hardness measurements, which showed that the material from the hot spot had been austenitized and subsequently cooled rapidly.

During the sample removal effort, tears or cracks were found in the cladding of the vessel around three nozzles. ANL analyzed vessel steel samples containing these cracks and found that the cracks penetrated only superficially into the base metal. The cracks were attributed to hot tearing of the cladding caused by differential thermal expansion between the stainless steel cladding and the carbon steel vessel that occurred during vessel cooling. Furthermore, the presence of control assembly material (Zr, Ag, Cd, and In) within the cladding tears and intergranularly on the surface of some sample locations indicated that a layer of debris containing metallic material was already present on the lower head when the

major relocation of ceramic molten core material to the lower head took place at 224 minutes after the initial reactor scram.

NOZZLE EXAMINATIONS

Fourteen nozzles and two guide tube specimens were extracted from the vessel by being cut off as close to the lower head as possible. Four nozzles in the hot spot region were melted off almost flush with the vessel and could not be removed. The damage states of the nozzles and guide tubes and their location with respect to the hot spot are shown in Fig. 6.

The nozzles and guide tubes were removed and shipped to INEL; six were then shipped to ANL for

examination. Examinations included micro and macro photography, optical metallography, scanning electron microscope measurements, gamma scanning, melt penetration measurements, and microhardness. There were two primary purposes for these examinations. First, these examinations would help to determine the extent of nozzle degradation to evaluate the thermal challenge to the lower head. Second, they would provide information on the movement of molten core material onto and across the lower head during the relocation. Portions from selected INEL nozzles and guide tubes were later sent to CEA Saclay, France, where similar examinations were performed.

Examinations performed on the nozzles and guide tubes, conducted primarily at ANL, provided insights

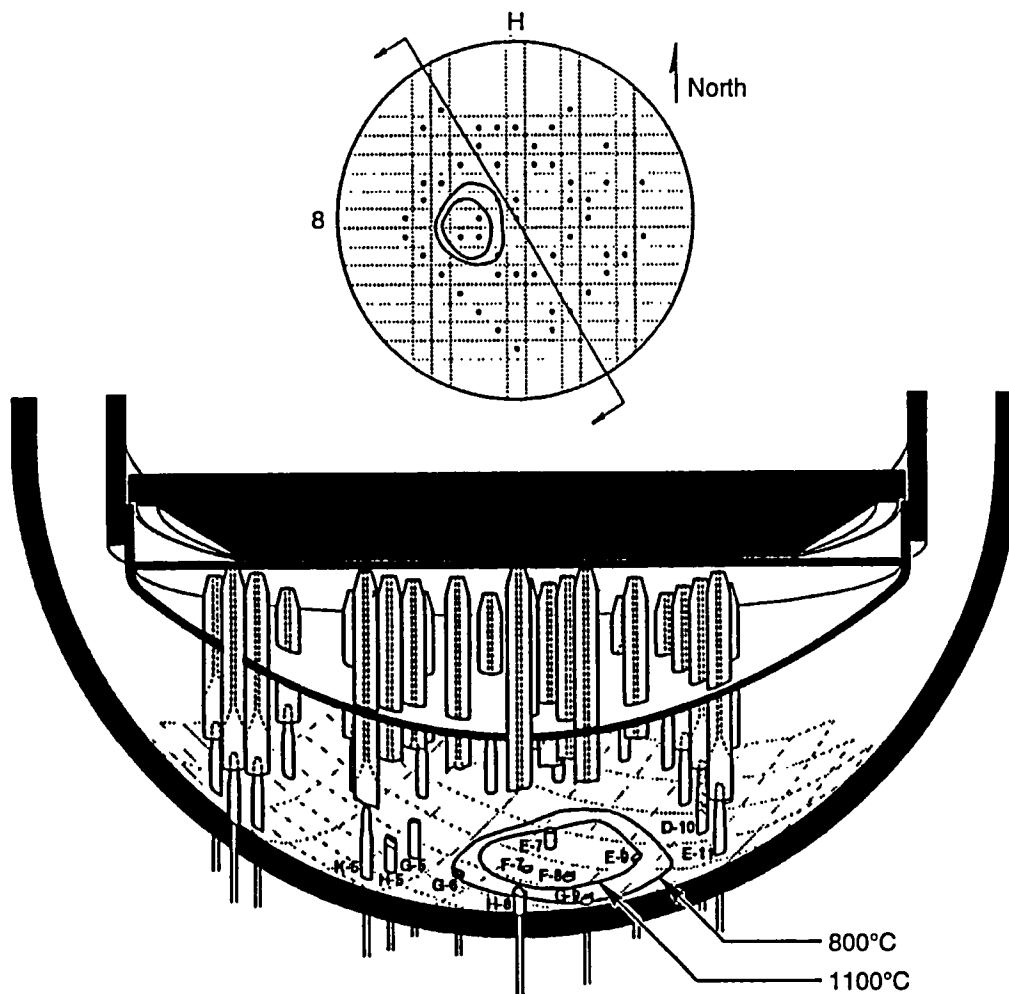


Fig. 6 TMI-2 lower head, southwest section.

into the accident progression. Damage to several nozzles indicated that their end-state condition was caused by molten core material coming in contact with the nozzles at an elevation ranging from 140 to 270 mm above the lower head. Surface scale found on the nozzles below their melt-off points suggested that this molten material flowed on top of a crust of preexisting solidified debris that had been cooled below its solidus temperature.

During the examinations it was estimated that nozzle temperatures varied widely as a function of location and elevation above the lower head. They ranged from 1415 °C, which is the Inconel 600 nozzle's liquidus temperature, to 1000 °C at elevations of 140 and 64 mm above the lower head, respectively. The penetration of debris downward into the nozzles was probably influenced by the temperature of the molten material at the time of entry, debris composition (and hence its fluidity), and the temperature of the nozzle itself. Temperature was found to greatly affect the solidification of molten debris and also the degree of interaction between the debris and the nozzle.

Examination results also indicated the presence of Zr and Ag-Cd on nozzle surfaces, which interacted with the material. The presence of this material indicated that control-rod material had relocated before the primary fuel relocation. The early movement of control material to the lower head was substantiated by the presence of control assembly material found in the cladding tears. However, it was not possible to determine the quantity of these materials that had relocated.

COMPANION SAMPLE EXAMINATIONS

The debris samples examined as part of the VIP were known as companion samples because they came from the hard layer that was in contact with the lower head. Hence they were "companions" to the lower-head steel samples. Results of the companion sample examinations were used to determine the debris composition and to estimate the lower-head decay heat load. During the defueling process, it was discovered that the hard layer was indeed extremely hard and had to be broken into pieces for removal. However, there was virtually no adherence of the material to the lower head itself. Because the hard layer had to be broken into pieces during sample acquisition, information on the sample location was limited to identifying the quadrant from which the sample was obtained.

The primary constituents of the companion samples were uranium, zirconium, and oxygen (U, Zr)O₂ with only small percentages (<1 wt%) of other structural material, such as Fe, Ni, and Cr. Control-rod materials such as Ag, In, and Cd were present in low (<0.5 wt%) concentrations. The average sample debris density was 8.4 ± 0.6 g/cm³ with an average porosity of $18 \pm 11\%$. Overall, the examinations indicated that the companion samples were relatively homogeneous with small variations in composition and density.

On the basis of the debris composition, it is quite probable that the molten material reached temperatures greater than 2600 °C in the central core region before relocation. The temperature of the debris when it reached the lower head is not known. However, the material reached the lower head in a molten state, and results of the examinations suggest that portions of the debris cooled slowly over many hours.

Radiochemical examinations indicated that the primary radionuclides retained in the debris bed were medium and low volatile constituents. Almost all the radiocesium, radioiodine, and radioactive noble gases volatilized from the molten core before it relocated to the lower head. Knowledge of the retained fission products is critical to estimating the debris decay heat and the resulting heat load on the lower head. Decay heat calculations indicated an overall heat load of $0.13 \pm 20\%$ W/g of debris when the relocation occurred at 224 minutes after scram and $0.096 \pm 20\%$ W/g at 600 minutes after scram. At the time of relocation, the total decay heat load was approximately 2.47 MW for the estimated 19 000 kg of material that relocated to the lower head.

The average burnup of the TMI-2 core at the time of the accident was relatively low. If the accident had occurred with the core near its end of life, the debris would have had a higher decay heat load. Although more volatile fission products would be retained in higher burnup fuel, calculations indicate that the decay heat for relocated fuel from a full burnup core would increase by less than 20% above that for the TMI-2 accident for the time period of concern (i.e., the first 16 hours after reactor scram).¹¹ Such a change in decay heat level would not have significantly altered the results of the margin-to-failure analysis or the conclusions of the VIP.

MARGIN-TO-FAILURE ANALYSIS

The final element of the VIP, the margin-to-failure analysis, was performed to investigate mechanisms that could potentially threaten the integrity of the reactor

vessel and to help improve understanding of events that occurred during the accident. Analyses addressed mechanisms that could result in lower-head penetration tube and vessel failures. Specific failure modes examined were instrument tube rupture, tube ejection, localized vessel failure, and global vessel failure.

Margin-to-failure calculations relied upon three major sources of VIP examination data: (1) nozzle examination data for characterizing melt composition and penetration distances within instrument tubes; (2) companion sample examination data for characterizing debris properties (e.g., decay heat and material composition); and (3) vessel steel examination data for characterizing peak vessel temperatures, duration of peak temperatures, and vessel cooling rate.

The margin-to-failure analyses provided significant insights into potential failure mechanisms of the TMI-2 lower head. Results of these calculations eliminated tube rupture and tube ejection as potential failure mechanisms during the accident. Melt penetration results indicated that ceramic melt did not penetrate below the lower head, which effectively eliminated ex-vessel tube rupture as a failure mechanism. Analyses also indicated that the instrument tube weld would remain intact even if the peak reactor coolant system (RCS) pressure were conservatively assumed to occur at the same time the hot spot formed. As a result, tube ejection was also eliminated as a potential failure mechanism.

Calculations indicated that the magnitude and duration of hot spot temperatures estimated in TMI-2 vessel examinations could not have been caused by an impinging jet. Rather, hot spot temperatures were due to a sustained heat load from debris on the lower head.

Because of insufficient available data, it was not possible to come up with a best-estimate quantification of the margin to failure for global or local creep rupture of the lower head. Such failures would be associated with high-temperatures on the lower head coincident with high reactor coolant system pressure. However, an extensive series of analyses and calculations was performed¹⁰ with the best available information to try to scope the issue as described in the following text.

The potential for the vessel to experience a global failure was evaluated for temperature distributions obtained from thermal analyses with best-estimate and lower-bound input assumptions for such parameters as debris decay heat, outer vessel heat-transfer coefficient, and the debris-to-gap heat-transfer resistance. Calculations for both of these cases indicated that global failure caused by creep rupture was predicted to occur within the

first 2 hours after debris relocation because of the sustained high vessel temperatures when the RCS was repressurized. This rise in RCS pressure occurred when the plant operators closed the block valve for the power-operated relief valve at 320 minutes after reactor scram.

Localized vessel failure analyses indicated that it is possible to withstand the 1100 °C hot spot temperatures for the 30-minute time period inferred from the vessel steel examinations provided that the rest of the vessel (i.e., outside the area of the hot spot) remained relatively cool. Localized calculations also indicated that the predicted time to vessel failure was reduced when a localized hot spot was superimposed on the calculated best-estimate background temperature (i.e., outside the hot spot).

Taken together, the localized and global vessel failure calculations indicated that the background vessel steel temperature behavior, which greatly depends on the heat load from the relocated debris in the lower head, was key to predicting failure from either of these mechanisms. Cool background vessel temperatures can potentially reduce structural damage and preclude global vessel failure even at high pressure and in the presence of a localized hot spot.

Thermal and structural analysis results were dominated by input assumptions on the basis of companion sample examination data, which suggested that the debris experienced relatively slow cooling over a period of many hours. However, differences between these analysis results and data from the vessel steel examinations indicated that the entire lower head cooled within the first 2 hours after debris relocation. An energy balance that considered coolant mass flows entering and exiting the vessel supported the hypothesis that the debris cooled in the time period between relocation and vessel repressurization.

Although there are insufficient data to quantitatively determine the exact mechanisms that caused this cooling, scoping calculations were performed to investigate possible mechanisms that could provide this cooling. In these analyses it was assumed that the simultaneous presence of cracks and gaps within the debris provided multiple pathways for steam release (e.g., water may travel down along the gap and boil up through cracks). Results of these calculations indicated that a minimal volume of cooling channels within the debris and a minimal size gap between the debris and the vessel could supply the cooling needed to obtain vessel temperatures and cooling rates determined in metallurgical examinations. Such cooling is not currently modeled in severe

accident computer codes. Also, there are uncertainties in models that estimate the cooling of debris as it breaks up and relocates to the lower plenum through water. Some questions also remain regarding the best failure criterion to be used for predicting vessel failure. However, the uncertainties in the amount of debris cooling on the lower head appear to be more significant for quantifying the margin to failure of TMI-2 vessel than either the vessel failure criterion or cooling of debris as it relocates to the lower plenum. Because of these uncertainties, results of the margin-to-failure analysis should be viewed as providing insights into areas such as identifying the failure mode with the smallest margin during the TMI-2 event and emphasizing areas in which additional research may be needed in severe accident analysis.

CONCLUSIONS

Through the efforts of the VIP signatories who supported the project, numerous significant contributions were made that dramatically increased both the understanding of the extent of damage to the vessel lower head and the margin of structural integrity that remained in the vessel during the TMI-2 accident. The principal results and conclusions from this project are summarized below.

- Vessel steel examinations indicated that a localized hot spot developed in an elliptical region approximately 1 m by 0.8 m. In this region, the maximum temperature of the ferritic steel base metal near the interface with the stainless steel cladding was approximately 1100 °C. The steel may have remained at this temperature for as long as 30 minutes before cooling occurred. Temperatures 0.45 cm into the 13.7-cm-thick wall were estimated to be 100 ± 50 °C lower than the peak surface temperatures. Away from the vicinity of the hot spot, lower-head temperatures did not exceed the 727 °C transformation temperature.

- Nozzle examinations and postaccident visual examinations indicated that the major lower-head relocation flow path for molten material was from the northeast and southeast quadrants of the vessel lower head toward the hot spot location in the western sector.

- Large margins to failure existed throughout the TMI-2 accident for the failure mechanisms of tube rupture and tube ejection. In fact, calculational results indicated that tube rupture and ejection can essentially be eliminated as potential failure mechanisms.

- Analyses results indicated that a localized effect, such as a hot spot, can shorten the overall vessel failure

times caused by creep rupture. However, by itself it is unlikely to cause vessel failure for the temperatures and pressures that occurred in the vessel during the TMI-2 accident.

- Without modeling-enhanced cooling of the debris and lower head, the margin-to-failure scoping calculations indicated that lower-head temperature distribution based upon data from companion sample examination data would have resulted in vessel failure when the reactor system was repressurized by plant operators at about 300 minutes after reactor scram.

- Even though a definitive scenario describing the movement of molten debris and the formation of a localized hot spot cannot be determined, considerable evidence indicates that a debris layer containing both ceramic and metallic material insulated the lower head. The hot spot formed in a location where this layer had insufficient thickness to effectively insulate the lower head from the molten flow.

SIGNIFICANCE OF THE VIP FINDINGS

One of the most important implications of the VIP conclusions relates to accident management. The TMI-2 accident began with the main feedwater pumps' trip, an anticipated event. It was compounded by closure of the auxiliary feedwater system block valves, a human procedural error, and by the failure of the pressurizer relief electromechanical valve to close after the proper relief of excessive primary system pressure, an electromechanical fault. The operator action of reducing the high-pressure safety injection system flow turned the event in a very serious direction. The operator had erroneously interpreted the indication of rising pressurizer water level to mean that the reactor coolant system was nearly filled with water, whereas in actual fact it was becoming a saturated system with steam formation caused by the loss of primary coolant. The operators failed to regain control of events in the first 100-minute period short of severe damage, which was the first opportunity for accident management. However, the operators were successful in discovering and opening the auxiliary feedwater system block valves early in this period, a necessary condition for final stabilization and recovery. In the intervening period of time since the TMI-2 accident, the total set of actions carried out to improve the interface between control room person and machine, to increase emergency safety system reliability, to develop emergency symptom-oriented procedures, and to improve reactor

operator training makes a repetition of such a failure very unlikely.

In the subsequent severe accident phase of TMI-2, the operators, though halting and inexperienced in an unknown field of reactor operations, were finally successful in stabilization and recovery. They isolated the stuck-open pressurizer relief valve and reactivated the high-pressure safety injection pumps, which were also necessary conditions, and thus enabled restoration of cooling water and heat removal in the primary system. This was the second and more difficult opportunity for accident management. The operators had cooling water and emergency power and pumps at their disposal, and they used them. The core was not cooled immediately when cooling water flow was restored. A crust surrounded the molten ceramic pool and prevented water from penetrating and cooling the material. The ceramic pool and surrounding crust continued to grow for about 25 minutes after high-pressure injection cooling water flow was restored until the crust broke through at its side at 224 minutes into the accident. The molten core material subsequently cooled after flowing to the vessel lower head. The experience at TMI-2 thus validates the importance of accident management and perseverance in a strategy of delivering cooling water. But it is also now clear as a result of the VIP that the reactor vessel provided a previously unrecognized defense in depth for a severe accident that was, of course, essential to success.

To pursue this point further, the VIP has also shown that global creep failure of the reactor vessel could occur under conditions of high vessel temperature and high pressure. Therefore accident management procedures should recognize the following: (1) the importance of cooling water not only for the reactor core but also for limiting the reactor vessel wall temperature and (2) the need for controlling pressure to avoid vessel creep failure. There should be here a word of caution about energetic fuel-coolant interactions (FCI) that could challenge pressure vessel integrity. We know that such an interaction did not occur at TMI-2 (Ref. 3), but some work on FCIs indicates an increased potential for triggering an FCI at low pressure.¹³ Nevertheless, most experts today believe that depressurization should take priority over the FCI concerns. Work separate from the TMI-2 VIP is under way to address remaining questions about energetic FCIs.

As a follow-up to the TMI-2 VIP, additional research can confirm the conditions under which reactor vessel integrity is likely to be maintained during a severe accident. The cooling of the external reactor vessel, by

flooding the cavity surrounding the lower part of the reactor vessel, could reduce the potential for reactor vessel failure. Analysis of the effects of ex-vessel cooling or plant-specific design features, such as vessel support structures or insulation that could restrict the flow of coolant or steam around the lower head, were not part of the VIP. However, several logical follow-on programs to the VIP, both internationally and at NRC, are currently under way or are in the planning stages to address reactor vessel failure issues. Additional research could also improve the understanding and quantification of the cooling of debris by water on the lower head.

The participants among the NEA-OECD countries examined the evidence, analyzed it, and reached conclusions about the accident as far as was possible. The international support and cooperation among the project participants, both technical and financial, helped make the TMI-2 VIP a success. For example, independent examinations of the vessel steel samples at laboratories around the world corroborated the estimated steel temperatures in the hot spot, which added credibility to the findings and conclusions of this project. Analysis of the accident shows that the TMI-2 reactor vessel was more robust than experts believed 15 years ago when the accident occurred and that this fact has broad implications for the accident management and safety of light-water reactors.

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Relocation of Molten Material to the TMI-2 Lower Head^a

By J. R. Wolf,^b D. W. Akers,^b and L. A. Nelmark^c

Abstract: *This article presents one possible scenario describing the relocation of debris to the lower head of the Three Mile Island Nuclear Station Unit 2 (TMI-2) reactor vessel and is based on available plant instrumentation records and postaccident examination results. The scenario presented here is not the only potential debris relocation scenario, but it is consistent with information obtained from plant data, Vessel Investigation Project examinations, analysis efforts, and other TMI-2 programs. This scenario addresses debris relocation events chronologically and assesses factors that may have contributed to the end-state condition of the lower head, the damage to the structures in the lower part of the reactor vessel, and the debris on the lower head. Included is the initial movement of molten material from the core, through the reactor vessel core support assembly to the lower internals, and finally onto the lower head.*

INITIAL EVENTS

The initial event that affected the relocation scenario was the melting of control and fuel rods that occurred between 100 and 174 minutes when the upper half of

the core was uncovered.¹ During this period, fuel-rod cladding, control-rod cladding, and metal melted and drained down through the uncovered core and thus left intact fuel-pellet stacks and rubble. The cladding material flowed down through the core to form a metallic crust 10 to 15 cm thick at the lower core region.² This lower bound was at the water level near the lowest grid spacer and approximately 20 cm from the bottom end of the fuel rods. The water level was approximately 2 m above the lower head, which was the lowest level during the entire accident.

At 174 minutes, the 2B coolant pump was activated for 19 minutes. However, significant flow through the core lasted only for about 15 seconds before the reactor coolant system repressurized. This repressurization was due to Zircaloy oxidation and steam formation in the upper core debris bed, which was caused by injection of relatively cool water by the 2B pump. Jets of steam from this event caused damage to the southern and northern portions of the upper fuel assembly grid and transported debris to the top of the upper plenum,^{3,4} onto lead-screw surfaces,^{5,6} and onto several other horizontal surfaces in the reactor vessel.⁷ Examinations of the upper core debris indicated that the control-rod materials (Ag-In-Cd) were concentrated in particles smaller than 1 mm and would thus be susceptible to transport as a hydrosol.

As discussed in Ref. 6, the overall upper core debris region was composed of about 27 000 kg of material. Between 3 and 10% of this debris was less than 1 mm in diameter. Because particles less than 1 mm may be

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transportable as a hydrosol, quantities of loose debris from both control and fuel rods either settled directly in the lower part of the reactor vessel during quiescent periods or were transported through the reactor coolant loop by the 2B pump transient and settled in areas such as the lower head, where there was relatively low flow. Therefore, finding intergranular Ag-In-Cd in the surfaces of several nozzles and in the vessel cladding cracks should not be unexpected. Unfortunately, the amount of such material and the depth of the deposition layer on the lower head cannot be definitely determined.

RELOCATION TO THE LOWER HEAD

Between 224 and 226 minutes, several almost simultaneous events indicated that a major change in core configuration occurred and molten material relocated to the lower head in one continuous flow. The count rate of the neutron source-range monitor located on the outside of the reactor vessel increased sharply. Also, the simultaneous alarm of in-core self-powered neutron detectors (SPNDs) at all levels on the same instrument stalk suggested that a common point of damage occurred. The molten material in the lower head heated the instrument nozzles sufficiently to produce thermoelectric currents in the SPNDs, which caused the instruments to set off an alarm. Examination of the alarm data⁸ indicated that the first alarms were for SPND stalks in instrument tubes on

the east side of the lower vessel and then propagated to the center. Postaccident measurements of in-core thermocouple loop resistance, as discussed in Ref. 8, indicated that new thermocouple junctions were formed in the lower head as the leads were melted by high temperatures caused by the relocated fuel. The new junctions also resulted in alarms of several of the in-core thermocouples. The alarms followed a sequence similar to the SPNDs. A primary system pressure pulse (2 MPa) also occurred during this time period. These data indicate the time when the relocation occurred and that it initiated in the eastern part of the core and lower head.

Movement of Molten Material Through the Vessel

Postaccident examinations of the eastern half of the core region and lower vessel internals confirmed plant instrumentation data and showed that relocation of the fuel debris to the lower head occurred in the eastern half of the vessel. Overall, about 19 metric tonnes of material reached the lower head. As discussed in Sec. 5, the relocated material was primarily a (U,Zr)O₂ ceramic. Visual examinations of this part of the vessel during defueling indicated that the primary path through the vessel was through a hole melted in the R6 vertical core-former wall and then downward through the horizontal baffle plates. Figure 1 shows a cross section of the reactor

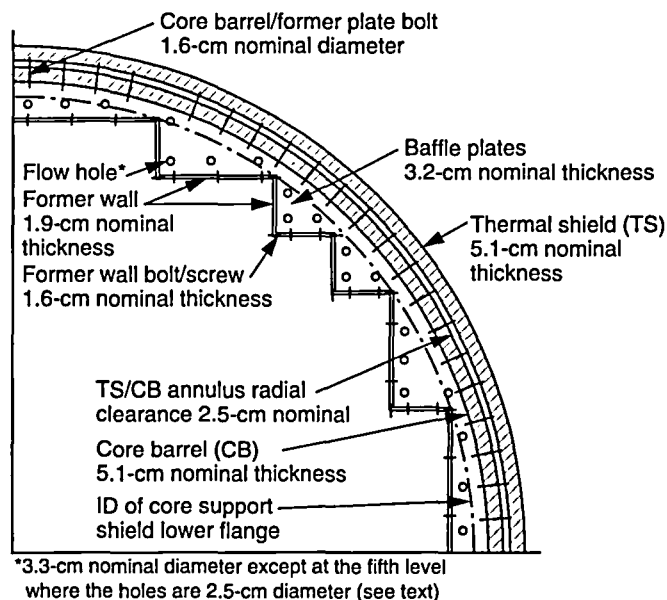


Fig. 1 Reactor vessel internal structure.

vessel internal structure. Fuel melt was found in the P-5 and R-6 assemblies near the bottom of the fuel assemblies, which indicated that some liquefied fuel had drained into these assemblies and solidified during the relocation. Because no flow path was found through these assemblies to the lower head, however, the principal relocation path was identified as being through the damaged core former at the R-6/P-5 core locations. Three holes in the core-former wall were identified. Dimensions of the holes through the former wall ranged from 23×3 cm to 20×7 cm.^{9,10} The damage to the core-former wall was approximately 140 cm from the bottom of the core, or a little below the midpoint of the reactor core. The damage location is indicated in Fig. 2.

Movement of Molten Debris Through the Core Support Assembly

At the bottom of the vertical core-former plates, the molten material melted back into the lower core support assembly (CSA). Visual observations indicate a massive hole and damage in the bottom on the vertical core-former wall located at core grid locations R-6, R-7, P-4, and P-5.

It is very difficult to trace the exact path the molten material took as it moved through the CSA structures. The flow movement scenario presented here is based on evidence derived from the assumption that the presence

of flow holes plugged with solidified material indicates that molten material flowed through these holes or adjacent holes during the relocation. Once a hole was plugged with solidified material, any subsequent material that flowed in that area was most likely diverted by the plug and flowed downward through an adjacent hole.

The CSA geometry consists of a number of plates and forging, as shown in Fig. 3. Once in the CSA, the majority of the molten material continued to flow down through the structures on the eastern periphery in the R-6/7 and P-4/5 areas. However, visual examinations indicated that some of the molten material was found to have flowed around the perimeter of the CSA structures as it penetrated downward toward the lower head. Figure 4 shows the location of solidified material at several locations in the flow holes of the lower grid, the area between the lower grid and the flow distributor plate, and between the flow distributor plate and the grid forging. The presence of solidified material is assumed to indicate that molten material flowed through or adjacent to these locations.

Molten Debris Movement on the Elliptical Flow Distributor

On the basis of the locations of solidified material in the CSA as shown in Fig. 4, it is postulated that the molten material flowed onto the elliptical flow distributor

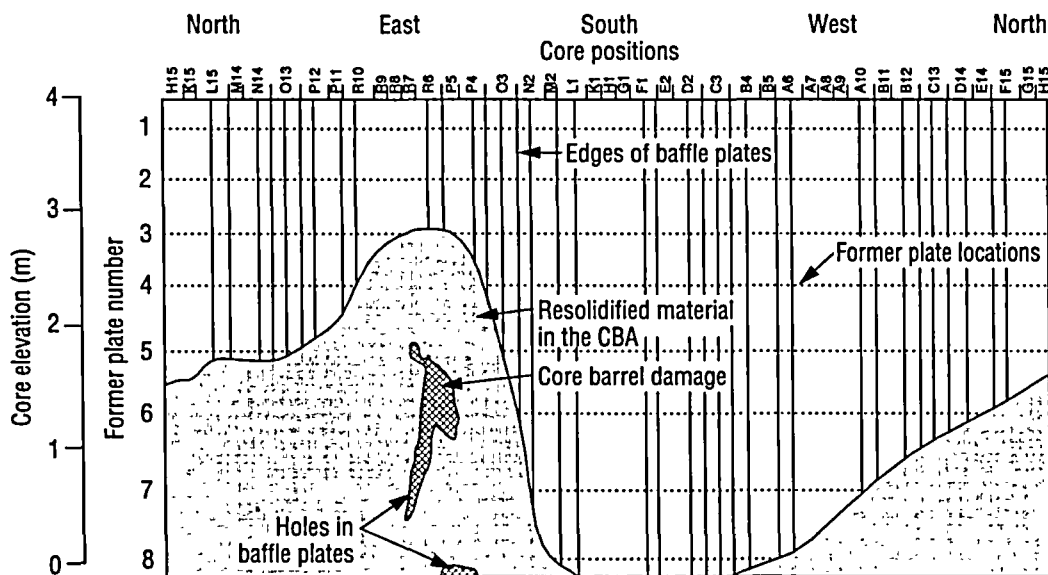


Fig. 2***Fuel debris profile inside core former (laid flat). CBA is core barrel assembly.

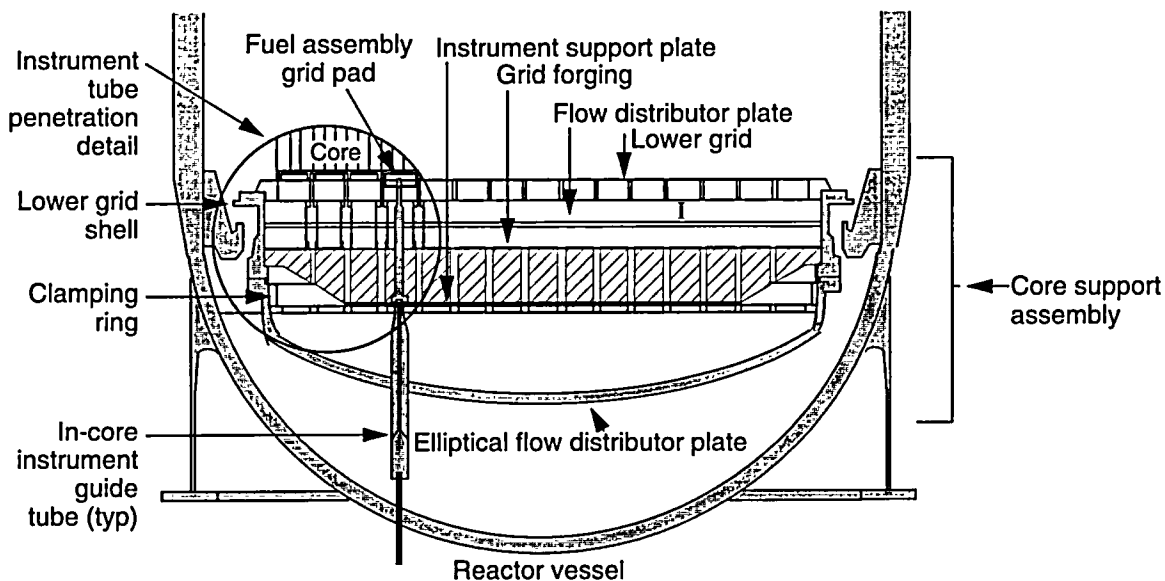


Fig. 3***TMI-2 core support assembly.

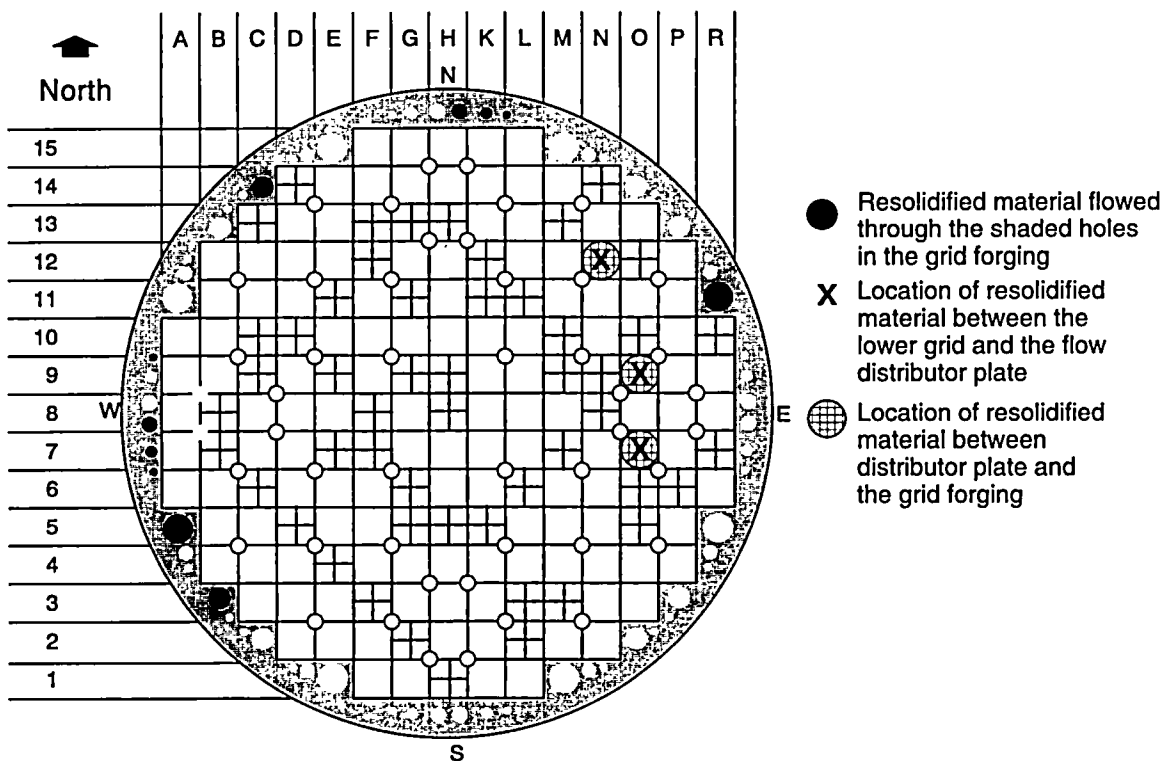


Fig. 4***Solidified material in core support assembly.

(EFD) from the same areas where plugged flow holes existed in the CSA. Figure 5 indicates the locations in the EFD where solidified material was observed in or above a flow hole.^{11,12} As shown in the figure, these locations are in general agreement with the locations in Fig. 4, where solidified material was observed in the CSA. As shown in Figs. 4 and 5, many of the plugged flow holes line up quite well, which indicates that the flow moved vertically downward and covered much of the periphery of the CSA structure as it followed the flow hole alignment pattern onto the EFD; for example, the plugged holes near locations H-15, K-15, and L-15 shown in Fig. 4 are near plugged locations H-15, K-15, and K-14 shown in Fig. 5. Also, the plugged holes in location C-14 shown in Fig. 4 are near the plugged holes in locations D-13 and D-14 shown in Fig. 5.

The minimal amount of damage on the EFD suggests that the first material that reached the EFD, and subsequently the lower head, was probably relatively cool. The exact temperature depends on both the amount

of heat given up by the molten flow before it reached the EFD and the exact composition of the molten flow. As the flow moved downward toward the EFD and eventually the lower head, heat was lost to the melting of core-former structures and to water that filled the lower plenum region. If lower temperature phases were present in the molten material, especially in the initial portion of the flow that would tend to incorporate melted structural material, it would be possible for this material to be mobile at temperatures below the solidus temperature of $(U,Zr)O_2$. Microstructural and microchemical examinations of portions of the loose debris that were removed from the lower head before the Vessel Investigation Project (VIP)^{13,14} indicate that eutectic structures present in grain boundary phases could have had a solidus temperature that was considerably lower than that of the bulk $(U,Zr)O_2$ material. This low melting point compared with that of the bulk material suggests that the grain boundaries may have remained liquid after the grains themselves had solidified. This would have allowed

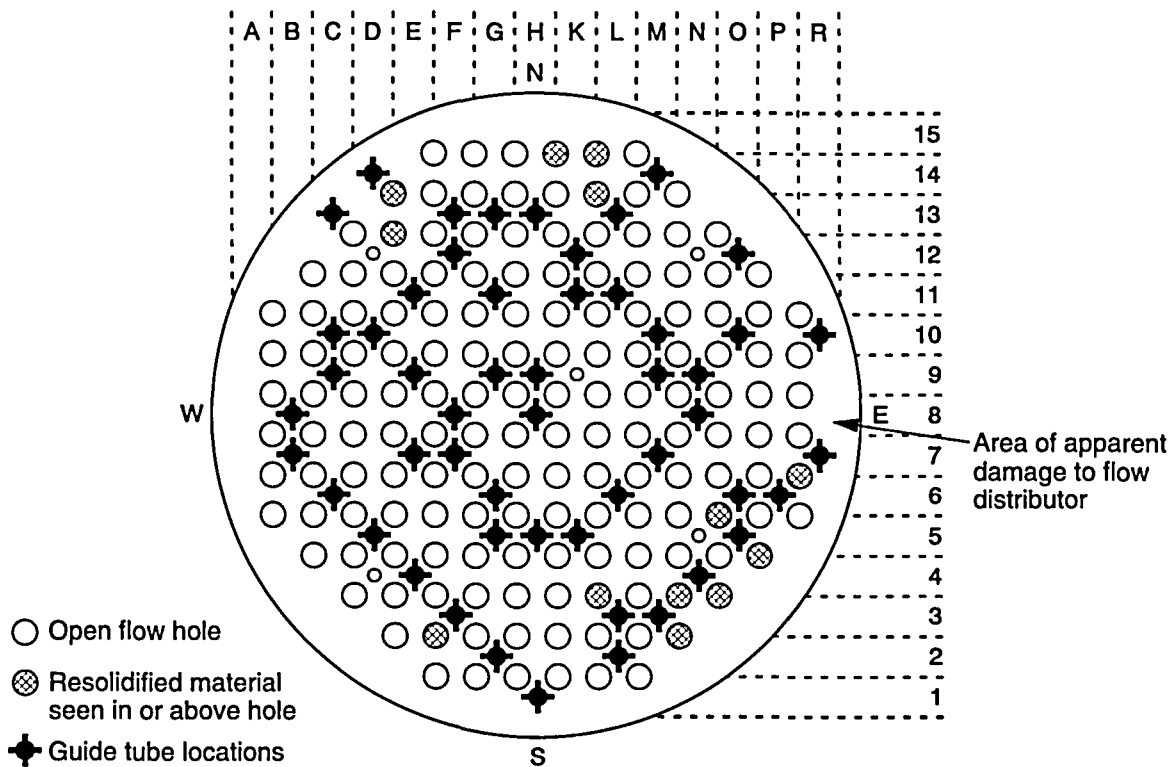


Fig. 5 Solidified material in the elliptical flow distributor.

portions of the molten relocation flow to remain mobile at temperatures below the bulk $(U,Zr)O_2$ solidus temperature.

Some of the molten material solidified on the EFD and formed plugs in the flow holes at locations shown in Fig. 5. The subsequent flow of material was probably diverted by the plugged holes and dropped onto the lower head from several different locations around the periphery of the EFD.

Movement of Molten Debris on the Lower Head

One of the most puzzling questions of the VIP has been why the molten material that relocated to the lower head did not do more damage to the vessel itself and why some nozzles were completely buried in solidified debris but showed absolutely no damage while others were almost totally destroyed. It is postulated that, when the initial portion of the continuous relocation flow reached the lower head, the combination of the heat sink provided

by the nozzles and the vessel lower head itself, along with insufficient thermal energy in the molten flow, cooled and rapidly froze the initial portion of molten material that reached the lower head. This made it possible for the rapid formation of a thick ceramic crust regardless of the temperature of the molten material. The rapid buildup of this crust resulted in the formation of an insulating ceramic layer that covered much of the lower head and also formed around many nozzles. Wherever the lower head and nozzles were covered by this insulating debris layer, they were protected from thermal damage.

As the initially cooler material fell onto the lower head from several different locations around the periphery of the EFD, the material effectively formed a cup-shaped basal crust structure that served to insulate the lower-head structures in these areas. Then hotter material flowed downward across the top of this basal crust and caused the nozzle damage pattern shown in Fig. 6. The pattern of nozzle damage indicates that multiple flow paths existed, and the movement of molten material onto and across the lower head was not one massive unified flow.

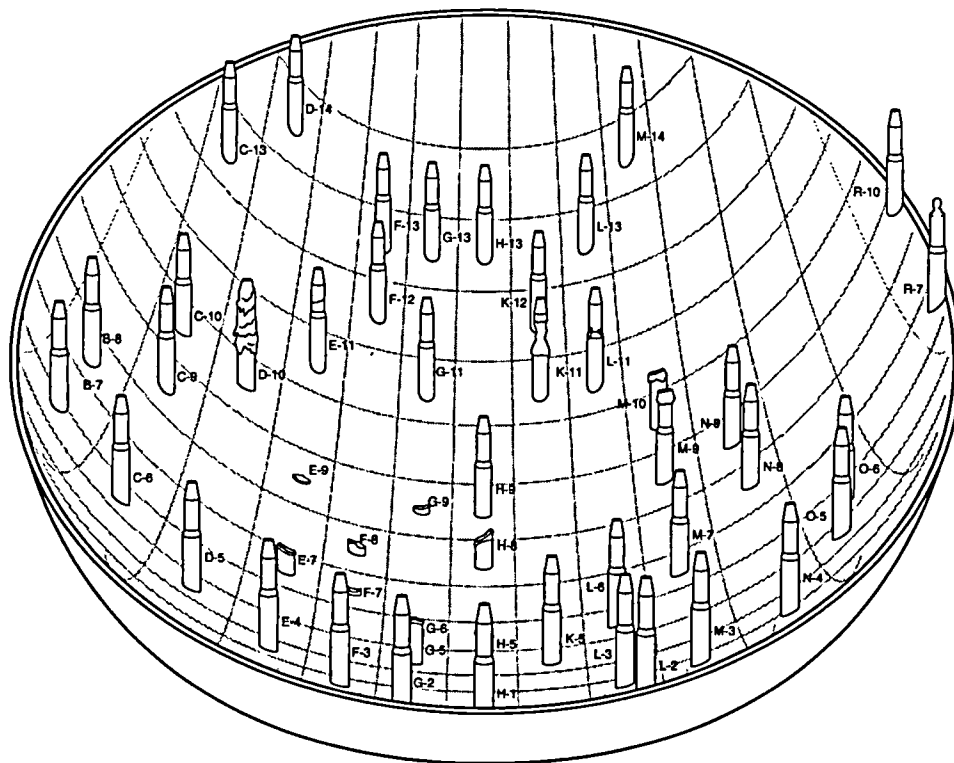


Fig. 6 Nozzle damage profile.

The pattern of nozzle degradation observed at elevated levels for several nozzles is shown in Fig. 6 and indicates the extent of the insulating ceramic debris layer that formed in the lower head and protected many of the nozzles and the lower head from extensive thermal damage. As the flow moved on top of the initial insulating debris layer, newly exposed molten fuel came in contact with the nozzles at elevated levels. These nozzles were melted at an elevation that is thought to be representative of the bottom of the molten fuel flow. Since the molten material flowed on top of the initial debris layer, this height is also representative of the thickness of insulating material that protected the lower head and the lower portions of many nozzles. As an example, examinations showed that the nozzle damage at M-9 was at about 25 cm above the lower head, and the damage to H-5 was about 15 cm above the head. Damage to nozzles around the M-9 and H-5 core locations, which have damage at elevations above the base of the nozzles, suggests that the insulating layer was about 25 cm thick at the M-9 location and 15 cm thick at H-5.

As the hotter molten material flowed across the top of the insulating ceramic debris layer, the cup-shaped structure that had initially formed on the lower head began to be filled. In the end, this resulted in what is known as the hard debris layer, which is shown in Fig. 7.¹⁵ The debris

depths shown in this figure were determined from mechanical probing of the hard layer during the defueling operation.

The last material to flow onto the lower head was what is known as the loose debris layer. The depths of the loose debris layer are shown in Fig. 8 and were determined before the defueling effort began.^{16,17} The depths were determined by probing examinations and by analysis of videotapes taken of the lower-head debris. Figures 9, 10, and 11 show cross sections of the thickness of the hard debris layer at several representative locations. As shown in these figures, relatively steep cliff-like areas occur along the periphery of the debris bed, and both full-length and damaged nozzles are embedded in the debris.

Formation of the Hot Spot

In addition to damaging the nozzles on the lower head, the flow of the hotter molten material may have also resulted in the formation of the localized lower-head hot spot. It is postulated that, as the hotter material flowed down the sides of the cup-like shape that was formed by the initial insulating crust toward the bottom of the vessel, the insulating layer crust became progressively thinner. Eventually, the flow of hotter material reached an area where the basal crust thickness was insufficient to adequately insulate the lower head, and a localized hot spot formed. The location of the hot spot on the lower head is shown in Fig. 12.

The hypothesis that the hot spot occurred beneath a crust that was of insufficient initial thickness to protect the lower head is consistent with the observation that the deepest debris was found in other locations of the vessel rather than over the hot spot. A progressively thinner crust was also indicated by data from the nozzle examinations, which showed that more of the nozzle length was melted in the vicinity of the hot spot. The region where the most severe nozzle damage occurred was consistent with the location of the hot spot and indicated that the insulating layer was thinnest in this area.

COOLING OF THE LOWER HEAD

Metallurgical examinations conducted as part of the VIP indicated that at the hot spot location the lower head was heated to peak temperatures of approximately 1100 °C and indicated that the temperature was maintained at that level for approximately 30 minutes before cooling rapidly (50 °C/min).¹⁸

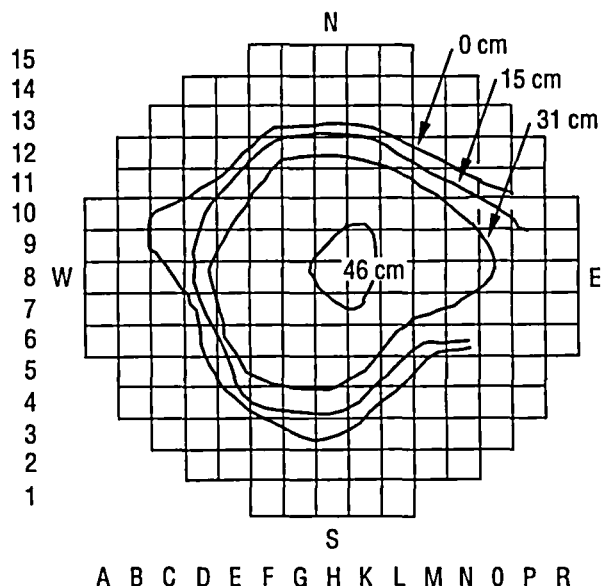


Fig. 7 Hard-layer debris depth. (Contour lines represent incremental increases. The outside line is equal to a depth of 0 cm and the inside is equal to a depth of 46 cm.)

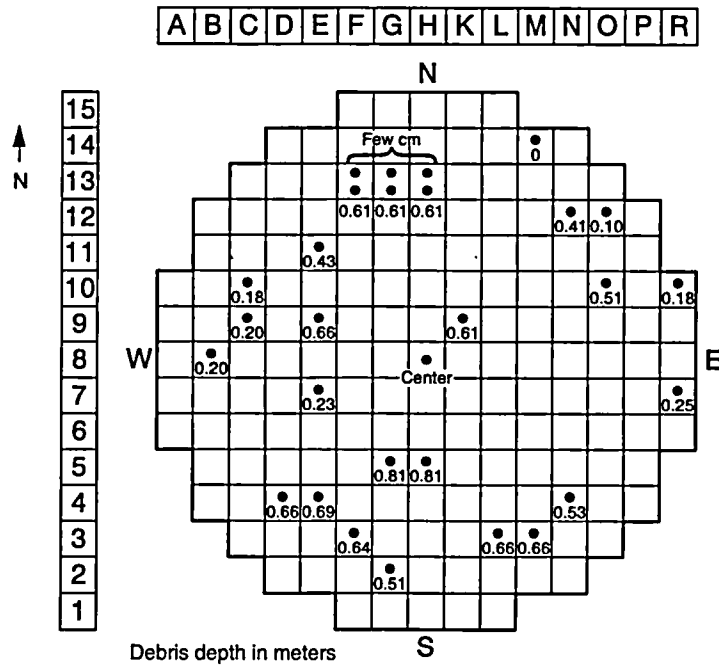


Fig. 8 End state hard- and loose-layer debris configuration.

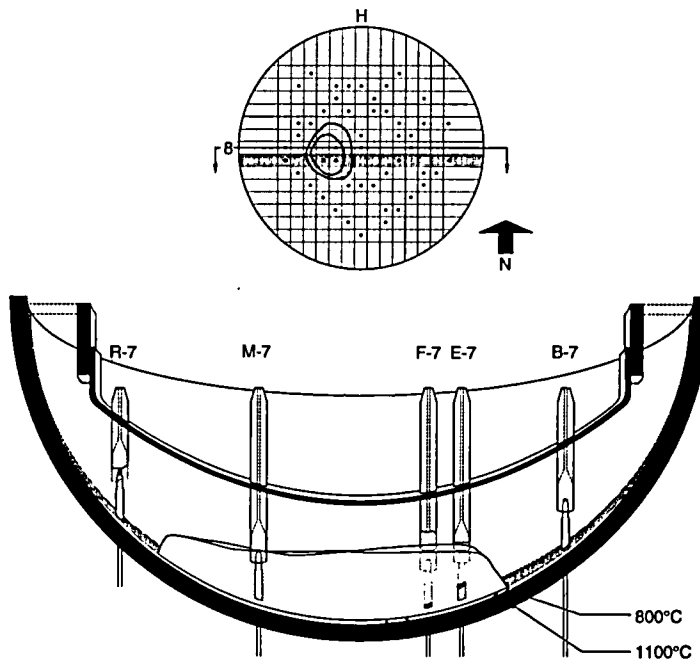


Fig. 9 TMI-2 lower-head cross section of hard debris, row 7.

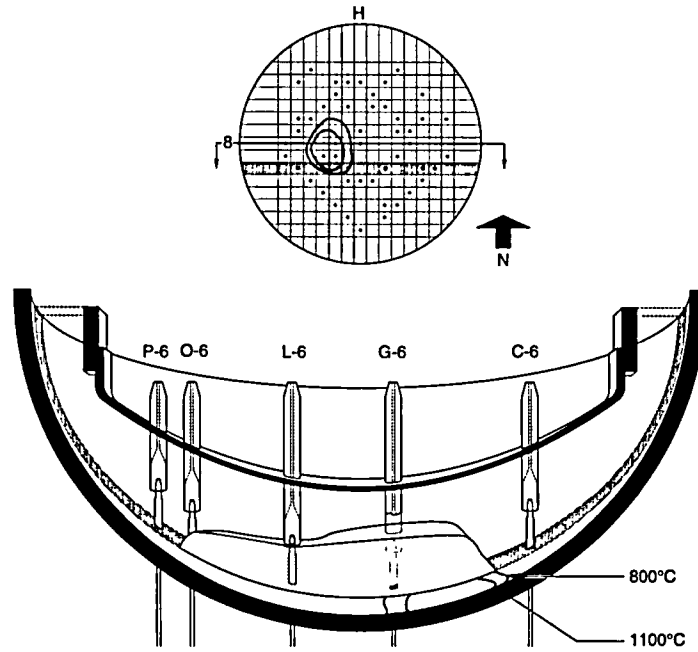


Fig. 10 TMI-2 lower-head cross section of hard debris, row 6.

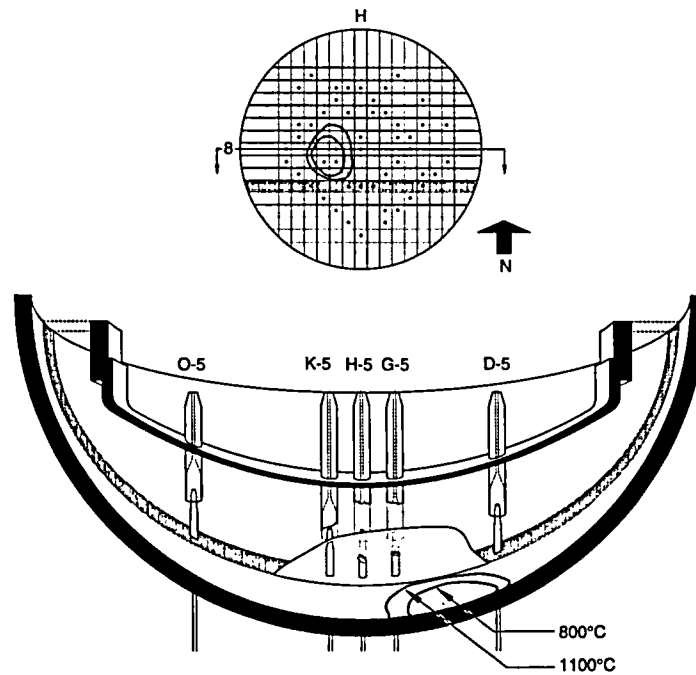


Fig. 11 TMI-2 lower-head cross section of hard debris, row 5.

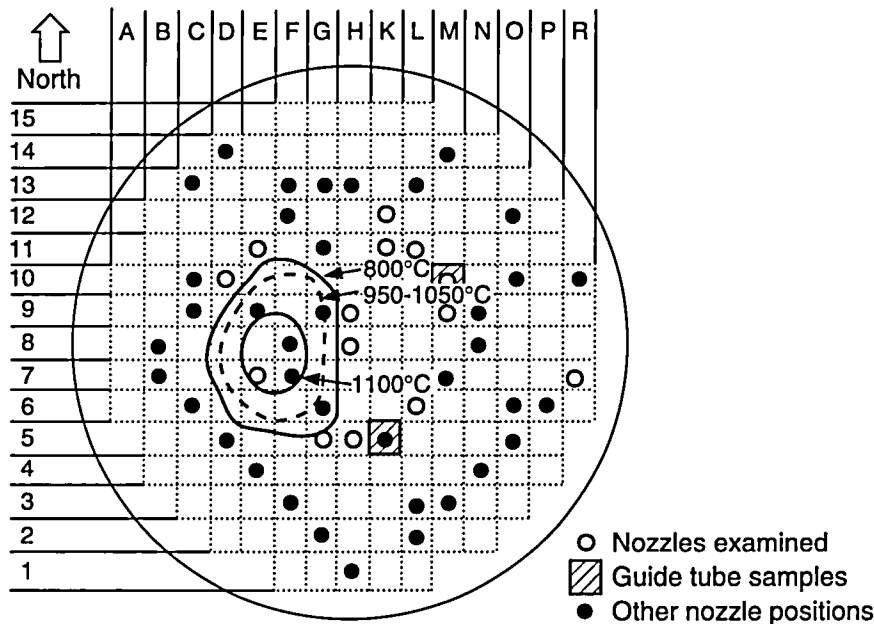


Fig. 12 Lower-head hot spot and nozzle-guide tube locations.

The mechanism responsible for the postulated rapid cooling of the lower head after 30 minutes has not been adequately explained. One proposed mechanism for this rapid cooling is the presence of interconnected flow channels within the debris and between the vessel and the debris layer. A considerable period of time (up to 30 minutes) would be required to adequately cool the peripheral portions of the debris before water could penetrate to the hot spot location. Upon penetration of water through gaps between the debris and the vessel wall, the vessel steel could have cooled rapidly, as indicated by the metallurgical examinations.

RELOCATION SCENARIO CONCLUSIONS

A scenario has been postulated on the basis of available plant instrumentation records and postaccident examination results. Although it is recognized that this scenario is not the only potential relocation scenario, it is consistent with information from plant data, VIP examinations, and analysis efforts. Key points of the scenario discussed in this section are:

- Relocating molten fuel flowed down through the core support assembly and onto the elliptical flow distributor plate.

- The initial molten fuel flow plugged holes around the periphery of the elliptical flow distributor plate and thus caused molten material to relocate from this plate to the lower head at several locations.

- The initial molten debris on the lower head cooled rapidly and formed an insulating layer of variable thickness that protected the lower head and many of the nozzles from damage.

- The pattern of molten material deposition on the lower head resulted in most of the vessels being insulated and protected from thermal damage. In the area just to the west of center (E-7, E-8, and F-8), however, the insulating layer was not sufficiently thick to protect the lower head, and thus a localized hot spot was produced.

- Effects, such as porosity in the insulating debris bed and cracking that occurred as the basal crust was formed, allowed water to penetrate into the debris bed to maintain some cooling.

- The hot spot remained hot for 30 minutes until water penetrated to the lower head between the crust and the vessel wall and caused rapid cooling of the vessel steel.

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Insight Into the TMI-2 Core Material Relocation Through Examination of Instrument Tube Nozzles^a

By L. A. Nelmark^b

Abstract: *The examination of instrument penetration tube nozzles removed from the lower head of the Three Mile Island Nuclear Station Unit 2 (TMI-2) reactor provided key information on the manner in which core debris relocated to and across the lower head. The examinations included visual inspections, gamma spectroscopy, metallography, microhardness measurements, and scanning electron microscopy. The examination results showed varying degrees of damage to the lower-head nozzles from $\approx$ 50% melt-off to no damage at all to nearby nozzles. The elevations at which nozzle damage occurred suggest that the lower elevations (near the lower head) were protected from molten fuel, apparently by an insulating layer of debris that had cooled and solidified when it reached the lower head. The pattern of nozzle damage suggests fuel movement toward the hot spot location in the vessel wall. Evidence was found for the existence of control assembly debris on the lower head before the massive relocation of fuel occurred.*

The 1979 accident at the Three Mile Island Nuclear Station Unit 2 (TMI-2) reactor resulted in the relocation of approximately 19 000 kg of molten core material to the lower head of the reactor vessel.¹ This material caused extensive damage to the instrument guide tubes and nozzles and was suspected of having caused significant metallurgical changes in the lower head itself. These changes and their effect on the margin to failure of the lower head became the focal point of an investigation cosponsored by the U.S. Nuclear Regulatory Commission (NRC) and the Organization for Economic Cooperation and Development (OECD). The TMI-2 Vessel Investigation Project (VIP) was formed to determine the metallurgical state of the vessel at the lower head and to assess the margin to failure of the vessel under the

conditions existing during the accident. The material in this article was developed under the VIP.

Under the auspices of the VIP, MPR Associates, Inc., removed specimens of the reactor vessel in February 1990.² In addition to these specimens, 14 instrument nozzle segments and 2 segments of instrument guide tubes were retrieved for metallurgical evaluation. The purposes of this evaluation were to provide additional information on the thermal conditions on the lower head that would influence the margin to failure and to provide insight into the progression of the accident scenario, specifically the movement of the molten fuel across the lower head.

Six of the instrument nozzle segments were examined at the Illinois site of Argonne National Laboratory (ANL)³ and eight were examined at the Idaho National Engineering Laboratory (INEL).⁴ The examinations at the two laboratories were complementary in that both laboratories received segments from different areas of the lower head which were representative of the range of damage that occurred to all the nozzles. Thus, from the nozzles that were examined in detail at ANL and from complementary data from INEL, it was possible to construct a scenario for the movement of the fuel debris across the lower head and to even obtain insight into how and where the fuel debris impacted on the lower head.

The original scope of the nozzle examinations at both ANL and INEL was geared to provide information that would aid in evaluating the thermal conditions of the lower head and thus aid the analysis of the thermal-mechanical state of the vessel and establish its margin to failure. To this end, the objectives of the examination were to (1) estimate peak temperatures of the nozzles from their metallurgical end state; (2) determine the mechanisms, modes, and extent of nozzle degradation to evaluate possible damage to the lower head; (3) determine the nature and extent (axial and radial) of fuel-debris ingress into a nozzle; (4) determine the nature and degree of chemical and thermal interaction among fuel, debris, and nozzles; (5) determine thermal-related

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metallurgical changes in the nozzles as a function of axial position to evaluate the axial temperature distribution and attempt to quantify temperatures near the vessel; and (6) determine the position and composition of debris adhering to nozzle surfaces to establish a "debris bed depth."

The nozzle segments received at ANL were from locations D10, E11, H5, H8, L6, and M9, indicated in the reactor grid plan shown in Fig. 1. These nozzle segments represented a range of thermal damage (i.e., melt-off and surface degradation) found in the 14 nozzles during the removal operations. Observation of the damage after removal of the core debris from the head revealed that nozzles in the area of E-H/7-9 were significantly more damaged than the nozzles around the periphery of the lower head. The degree of damage to individual nozzles would be indicative of the possible damage, or change in metallurgical condition, of the vessel close to the nozzle. Nozzle H8 was the most heavily damaged of those examined at ANL, having a length of only 70 mm and leaving a 51-mm-long segment, or stub, on the vessel. Nozzle L6, on the other hand, was 241 mm long and showed no outward damage. The other four nozzles exhibited either melt-off damage at different elevations (M9 and H5) or

different degrees of surface damage (D10 and E11). Thus examination of these six nozzles provided sufficient information and insight to satisfy all the objectives of the examinations and provided insight into the movement of the molten fuel across the lower head.

In this article, we report the examination findings and show how they lead to the conclusions on fuel relocation and its qualitative significance to the integrity of the lower head.

EXAMINATION METHODS

The examination methods used at ANL consisted of visual examination and macrophotography, axial gamma scanning for ^{137}Cs , macroexamination of cut surfaces, metallography, microhardness measurements, and scanning electron microscopy-energy-dispersive X-ray (SEM-EDX) analysis.

The nozzle segments were systematically sampled for detailed examination to obtain the desired data. Sectioned areas were based on the following attributes: (1) top and bottom locations, to obtain information on the hottest (sometimes molten) and coldest (nearest the vessel) temperature extremes in a nozzle; (2) fuel-nozzle interaction areas (nozzle degradation mechanism); (3) indications from gamma scans of fuel penetration into the nozzle; (4) obvious locations of debris on a nozzle; and (5) locations of surface cracking (nozzle degradation mechanism).

SIGNIFICANT FINDINGS

Pattern of Nozzle Damage

For the significance of the identified damage to be appreciated, the elevation of the damage to a particular nozzle above the bottom of the vessel must be considered. Figure 2 shows the relationships among the elevations of nozzle locations referenced to the lowest nozzle location at H8, and Table 1 provides the actual elevations for and segment lengths of the six ANL nozzles. These elevations are important to the understanding of how the molten debris moved on the lower head and caused the nozzle damage. Figure 3 shows the as-removed appearance of the six nozzles examined at ANL. Table 1 should be used to obtain a true comparison of the elevations at which nozzle damage occurred because the stub lengths remaining on the vessel were different for each nozzle. The tops of nozzles M9 and H5 clearly exhibited an appreciable amount of melting. The transition zone

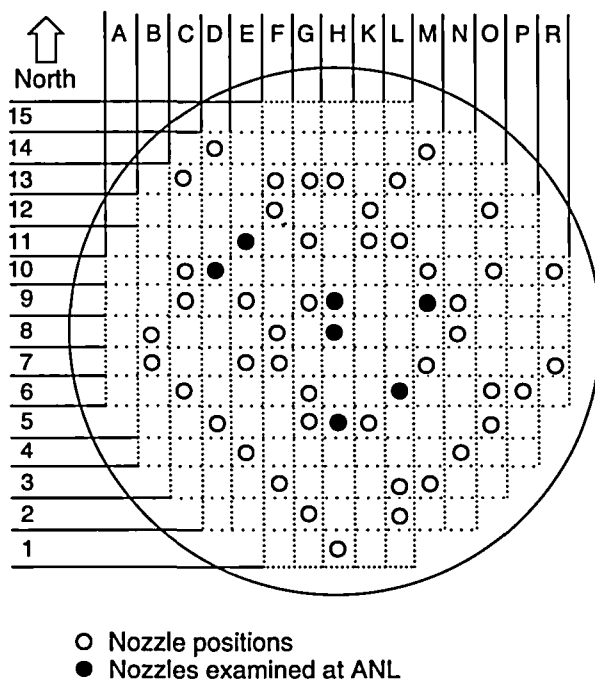


Fig. 1 Grid plan of TMI core showing positions of nozzles.

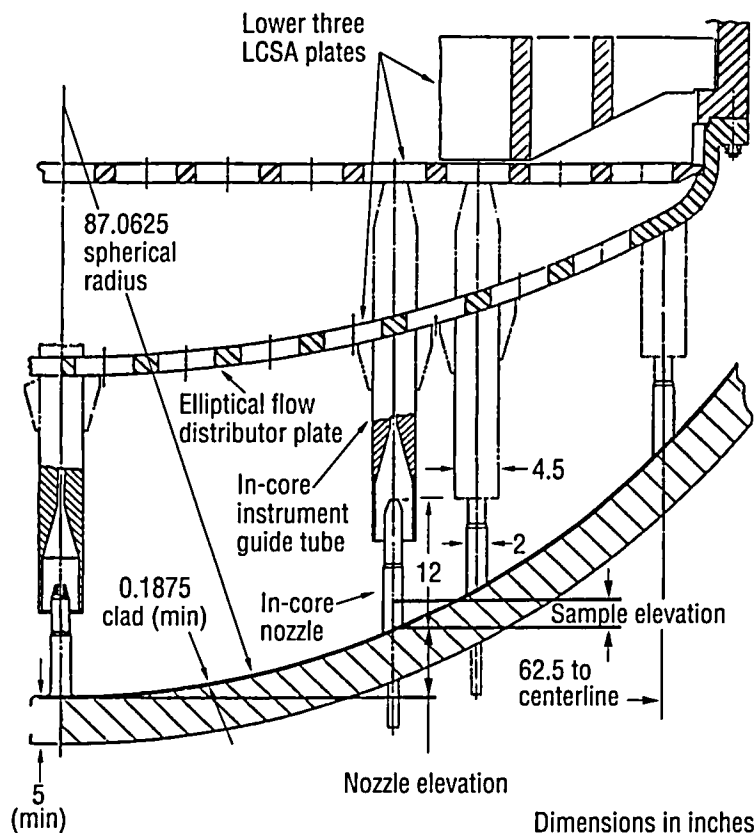


Fig. 2 Lower head area and in-core instrument guide tubes.

Table 1 Lengths, Elevations, and Fuel Penetration Depths of Nozzle Segments Examined at ANL

Nozzle	Elevation of nozzle base, mm	Segment length, mm	Stub length, mm	Elevation of top of segment, ^a mm	Fuel penetration elevation above nozzle base, ^b mm
M-9	119	254	26 ^c	280	241
L-6	94	241	64 ^c	305	75
H-5	107	146	0	146	89 max 117 min
H-8	0	70	51	121	<64
D-10	244	235	57 ^c	292	55 max 184 min
E-11	221	225	77 ^c	302	204

^aReferenced to nozzle base.

^bBased only on gamma scans.

^cCalculated as the difference between 305 mm and the sum of the two known values. Measurements of stub lengths for D10 and E11 from photographs were not deemed sufficiently accurate because of angle of photo.

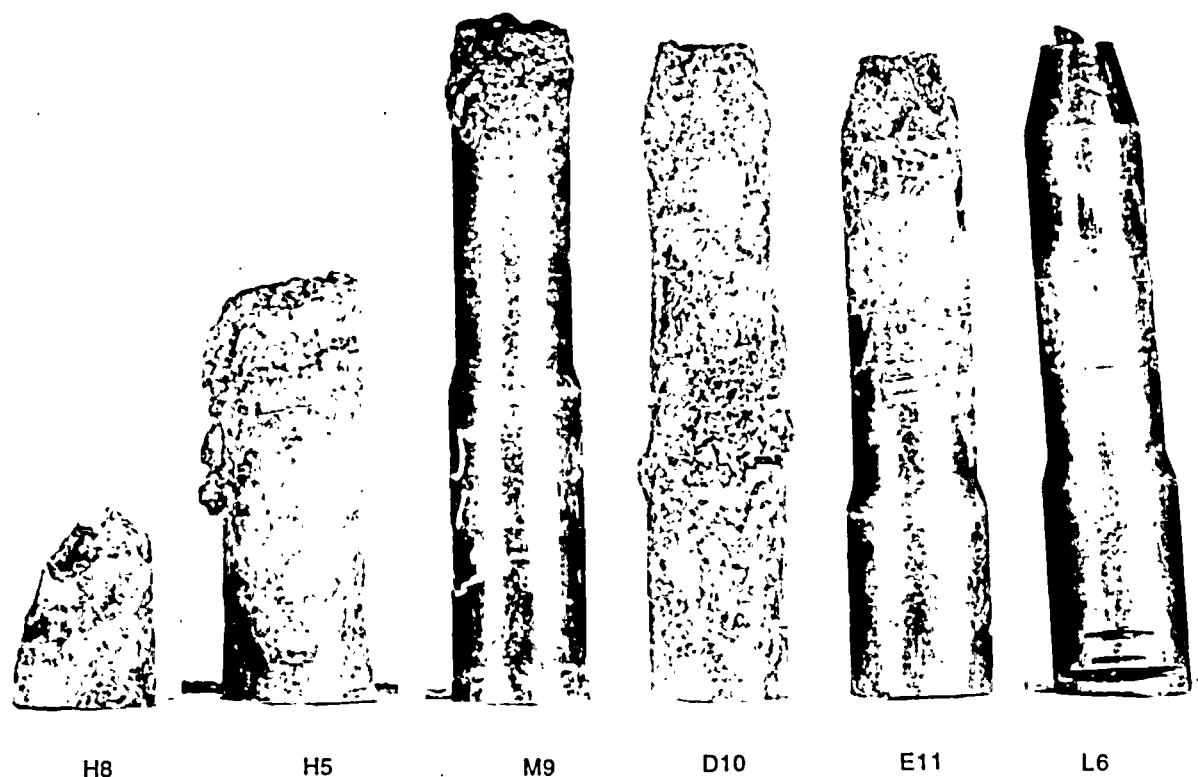


Fig. 3 As-removed appearance of six nozzles examined at ANL.

between the molten region and the unaffected lower part of the nozzles was relatively narrow on M9 and more extensive on the shorter H5. These transition zones were typically covered with a thin scale that was basically an iron oxide with entrapped shards of various core debris materials (Fig. 4); the lower areas of the nozzles were clean of adherent scale and showed little, if any, effects of being in contact with very hot core debris.

Significant fuel penetration into these molten nozzles was essentially limited to the melted and scaled elevations, i.e., the hot top of the nozzles. The material found in the top of nozzle M9 (Fig. 5) was a mixture of solidified fuel and nozzle remnants in a matrix of chromium oxide from the Inconel 600 nozzle material; this oxide was different from the iron-based oxide scale on the outside of the nozzles. It is believed that the ability of the fuel to penetrate downward into the nozzle was limited by the chromium oxide in which it was trapped (Cr_2O_3 melts at 1990°C).

The H8 nozzle segment received at ANL was only the bottom portion of a longer postaccident segment, the top of which was broken off during the removal operations.

The top surface of the bottom portion, shown in Fig. 3, 121 mm above the vessel surface, was smooth when compared with the melted regions of M9 and H5. Upon detailed examination by SEM-EDX analysis, it was found that this surface had reacted extensively with a molten iron-rich phase that contained ingots of silver-cadmium. These elements would have come from control assembly components that apparently melted early in the accident and were deposited on the lower head in advance of the major fuel flow at that location. Intergranular penetration of silver-cadmium was found in several nozzles and into the surface of the vessel cladding.⁵

In contrast to the melted condition of nozzles M9 and H5, nozzle L6 (almost midway between them on the lower head) showed no external damage at all. This indicates that the fuel movement in the lower head was not a unified flow but rather individual flows from various directions.

Although the surface of nozzle L6 was clean, the nozzle contained solidified fuel masses down to within 75 mm of its base, the deepest penetration into any



Fig. 4 Layer of debris on outer surface of nozzle D10 at the 82-mm elevation (magnification, 190 $\times$).

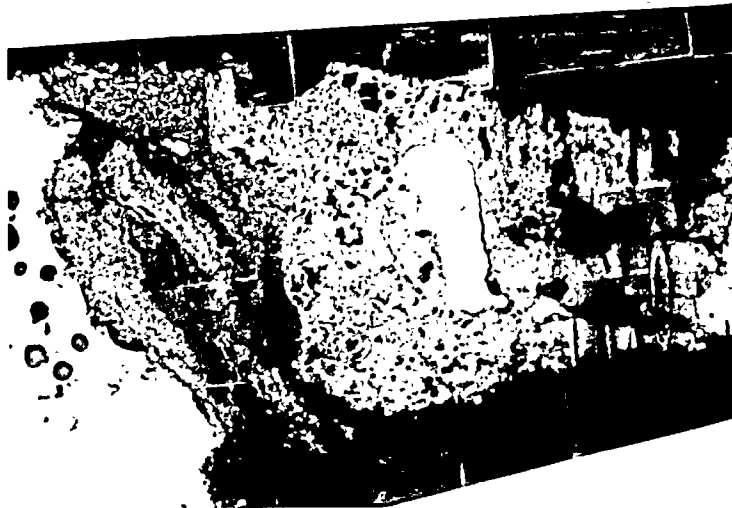


Fig. 5 Longitudinal section through top of nozzle M9 (magnification, 7 $\times$).

nozzle, 230 mm, from the apparent entry elevation. This deep penetration is attributed to the lack of fuel–nozzle interaction that would have formed a binding chromium oxide. Because both the nozzle and its overlapping guide tube were undamaged, the source of this fuel is not obvious: it appears to have been physically impossible for molten fuel to have traveled up under the guide tube and down into the nozzle without damaging either. It must be

concluded that the fuel came down directly through the guide tube from somewhere up in the reactor.

Nozzle D10 was at the periphery of the lower head and appears to have been on the edge of the flow of molten fuel. One side of the nozzle was heavily encrusted along its entire height, whereas the other side, in a 180° arc, showed only the more common light surface scale. When it was sectioned, it was found that an unexplained

internal pressurization had pushed out the hot, crusted side of the nozzle and thus made it egg-shaped in cross section. The internal pressure created a crack in the outer surface of the nozzle and also collapsed the inner Inconel 600 tube of the instrument string. The body of the nozzle had undergone intergranular hot tearing, which apparently penetrated to the surface and formed the crack. The nonuniform damage indicates that these events occurred quickly with no time for heat transfer to the rest of the nozzle. This could be expected at the edge of a fuel flow coming to rest against the nozzle.

The last nozzle, E11, was damaged only at its tip, below which was a fairly extensive area of the iron-based scale. Melting was limited to the inner and outer surfaces of the tip, and rapid melting and solidification were indicated. Fuel penetration was relatively deep (compared with that in M9, which also had Inconel melting), apparently because the temperature at the top was too low to form chromium oxide, which most likely would have limited downward fuel movement. Instead, the material in the tip of the nozzle was in an iron-based oxide similar to that of the surface scales.

Two principal conclusions may be reached from the variable degradation of the instrument tube nozzles. First, considering that most of the nozzles on the lower head were covered with a hard, solidified layer of fuel debris but nozzles such as L6 sustained no outward damage from contacting this debris, it can be concluded that much of this debris acted as an insulator and protector of both the nozzles and the lower head. The absence of virtually any indication of degradation in the bottom parts of nozzles (even in those whose tops had melted) indicates that what was likely the first fuel debris to reach the lower head solidified relatively quickly and built up a significantly thick insulating layer. Once this layer had built up, the later material arriving on top of the solidified material melted off the tops of those nozzles which were exposed. The elevations at which these melt-offs occurred provide evidence for the thickness of the initial protective layer at various locations around the lower head. Thus the fact that the nozzles in the vessel hot spot area of E-F/7-9 were melted down the most indicates that only an initially thin insulating layer existed there, which apparently was the reason the hot spot formed where it did.

The second conclusion is that the movement of fuel debris across the lower head was not one massive, unidirectional flow but more likely a number of flows from various directions. This derives from the lower-head locations where specific nozzles melted off and the

elevations at which they melted. The melt-off of M9, in the eastern side of the lower head at a relatively high elevation, indicates a thick initial debris layer, with subsequent hot fuel moving downward toward the reactor center atop this thick crust. Similarly, nozzles H5 and G5 were melted off atop a somewhat thinner initial crust, whereas nozzle L6 did not melt because it was initially totally covered with debris that had already solidified. These crust thicknesses are very likely indicative of the amount of molten core material that initially solidified on these locations, and indeed these locations correlate with the locations in the elliptical flow distributor through which debris is believed to have come. The initial debris from the major fuel relocation apparently impacted the lower head around the periphery, upward on the vessel curvature, and formed a cup-like debris mound that solidified rapidly. Debris flowing downward, lava like, atop initial crusts at M9 and H5 would effectively be moving toward the area of the short, melted-off nozzles where the vessel hot spot occurred.

Penetration of Materials Into Nozzles

The penetration of gamma-active materials downward into the nozzles was estimated from the ^{137}Cs gamma activity profiles; the results are summarized in Table 1. The gamma activity was assumed to be associated with fission products in the fuel, and therefore the results are reported as "fuel penetration." Metallic debris, essentially molten Inconel from the nozzle, was also found in the nozzles but not tabulated.

Although porous, ceramic-appearing material was seen in the as-cut transverse sections at elevations below the nozzle tops (e.g., in H8 and L6), there seemed to be difficulty in retaining this material during the subsequent sectioning operations to form metallographic mounts. This finding attests to the friable nature of the material. In most cases fuel material that was retained at the lower elevations exhibited two features. First, it appeared to be in the early stages of transformation to uranium-rich and zirconium-rich phases, which indicated relatively rapid cooling. Second, it contained iron, aluminum, and chromium in the grain boundaries, which indicated likely fluidity significantly below 2000 °C, which would aid the fuel's mobility to the elevation where it finally solidified.⁶

In nozzles M9 and H5, which melted off, the penetration was shallow, which indicates a quick melting and relatively rapid cooling, notwithstanding the phase transformations in the fuel areas. It is likely that the melting point of chromium oxide dominated the mobility

of this material before thermal equilibrium and lower melting eutectics could form. The phase transformation of the fuel would have occurred below 1990 °C while the solidified fuel was trapped in the insulating chromium oxide. In contrast, porous fuel was found at the base of the H8 nozzle segment, far below where the nozzle apparently had melted (i.e., in the part of the nozzle not received at ANL). This fuel may have entered the breach where the nozzle had interacted with liquid zirconium and at too low a temperature to form chromium oxide.

The fuel in the tops of nozzles D10 and E11 differed from that in nozzles M9 and H5 in that it was trapped in an iron-based rather than a chromium-based matrix. This reflects two probabilities. First, the Inconel did not readily give up its chromium to oxidation, probably because the temperature was too low. Second, the source of the fuel and the iron-based matrix was probably the same as that of the iron-based surface scales. That many of the fuel particles were shards and not solidified in situ masses indicates that the fuel flow in this region of the vessel was cooler than the flow that contacted nozzles M9, H5, and H8. This is consistent with a scenario that has the fuel flow coming to the vessel hot spot from the east and south and piling up on the far side against nozzles D10 and E11. (Note that the surface crust and major heating load was on only one side of D10.)

Presence of Control Assembly Materials

Four of the six nozzle segments examined at ANL were under control rod assemblies: M9, L6, H5, and H8. One, D10, was beneath an axial power-shaping rod that contained 914 mm of Ag-In-Cd clad in stainless steel. The last, H5, was beneath a burnable poison rod that contained $\text{Al}_2\text{O}_3\text{-B}_4\text{C}$ pellets clad in Zircaloy. There is pervasive evidence from the ANL examinations that materials from assemblies containing Ag-In-Cd were deposited in some form, probably as solid particulates, on the lower head before the principal fuel flow occurred at 226 minutes. Unfortunately, there is no direct, unequivocal evidence that a bed of control rod debris existed on the lower head. Most, if not all, of such a bed of control rod debris would have remelted and possibly been consumed when it came in contact with even the initial, cooler, fuel that reached the lower head first. Therefore evidence for such a bed would now be, at best, on a microscopic scale and fortuitously derived.

The first evidence that the control materials were on the lower head before the fuel flow arrived was the finding of Ag-Cd nodules and In-Fe-Ni-Zr phases solidified in situ in the cracks of the vessel cladding of the E6 and

G8 boat samples.⁵ Second, the liquid that ablated nozzle H8 was overwhelmingly zirconium-rich and contained silver-cadmium masses. The zirconium-to-uranium ratio of approximately 8.5:1 was far in excess of the zirconium-to-uranium ratios found in fuel masses that were analyzed. This excess of zirconium would be from the Zircaloy shroud tubes in the control assemblies. Minimum depth of the zirconium-containing debris bed at this location would have been approximately 120 mm. Third, the findings of silver and silver-cadmium inclusions deep beneath the surfaces in most of the nozzles in a form of liquid-metal penetration indicate there was a layer of control materials either adhering to the surface ready to be melted when contacted by the hot fuel or there was a thick debris bed up against the nozzle that would yield the same result. That liquid silver-cadmium had penetrated the Inconel nozzles somewhat before nozzle melting occurred is supported by the apparently vapor-pressure-derived bubbles containing silver-cadmium deposits in the molten Inconel tops of some nozzles (see Fig. 5). Finally, the finding of a layer of 10- μm particles of silver-cadmium beneath a fuel debris scale on nozzle E11 indicates predeposition of control materials.

The significance of a bed of control material debris could be twofold. First, intergranular penetration of the vessel cladding by silver-cadmium may have played a role in the hot tearing of the cladding. Second, interaction of control material with nozzle material was at a low elevation, which may have allowed greater penetration of molten fuel into nozzle H8 than otherwise would have occurred. A third consideration, a potential insulating effect of the debris bed on the thermal impact to the vessel, was not supported by a heat transfer analysis performed at INEL.

EXAMINATION CONCLUSIONS

- The nature of the degradation of nozzles M9, H5, and H8 indicates that their melt-off was by liquid fuel approaching the nozzles at elevations of approximately 140 to 270 mm above the lower head. Surface scale on the nozzles below the melt-offs suggests that the liquid was atop a crust of solidified and partially solidified debris that had been cooled below its solidus, initially by the water in the lower head and finally by contact with the lower head.

- The flow of very hot material on the lower head followed multiple paths. The damage to nozzles M9, H5, and H8 suggests that flows occurred from the east and

south but apparently did not affect nozzle L6 because it had already been covered by cooler material that had reached the lower head first.

- The pattern of nozzle degradation and the assumed directions of fuel flow are consistent with a vessel hot spot at E-F/7-8, where there was apparently only a thin protective crust.

- The fuel debris in and on nozzles D10 and E11 and the one-sided degradation of D10 suggest that these nozzles were at the periphery of the fuel flow.

- Nozzle temperatures ranged from 1400 °C (melting) at 140 mm from the vessel at H5, down to approximately 1000 °C, based on a nickel-zirconium eutectic temperature of 961 °C at 64 mm from the vessel at H8.

- In addition to melting, nozzle degradation mechanisms were ablation by liquid zirconium, intergranular penetration by zirconium and silver-cadmium, chemical interaction with aluminum, chromium depletion caused by extensive oxidation, and internal pressurization that caused hot tearing and nozzle ballooning.

- The presence of significant quantities of zirconium and silver-cadmium on the vessel that interacted with the nozzles is attributed to the prior deposition at that location of control assembly material. The depth or nature of such a debris bed could not be confirmed, but the depth is estimated to have been at least 120 mm at the H8 location.

- Penetration of fuel debris downward into the nozzles was influenced by the temperature of the fuel at the time of entry; by the composition, and hence the fluidity, of the fuel; by the temperature of the nozzle and its ability to solidify the debris; and by the degree of interaction between the fuel and the molten nozzle to entrap the fuel in chromium oxide.

SIGNIFICANCE OF FINDINGS

Perhaps the most significant finding of the nozzle examinations and the examinations of the surfaces of the vessel samples was the lack of evidence of molten-fuel contact with the vessel surface. This would indicate that the temperature of the fuel debris that contacted the vessel surface had already dropped below the solidus temperature while the fuel moved through the water. The only evidence for molten material on the lower head was that for control rod constituents in both the nozzles and the vessel cladding. Much like volcanic lava flows entering the sea, an insulating crust was formed and kept the internal molten material contained and thus away

from the vessel. The presence of water in the lower head, therefore, was paramount in mitigating the consequences of the accident. It follows that molten fuel entry into the lower plenum is not tantamount to failure of the lower head because of being contacted by molten fuel if water is present.

The fuel debris that eventually reached the lower head apparently took a circuitous path from its initial core location, and contact with reactor internals along the way likely extracted significant thermal energy.⁷ Evidence was present for multiple pour locations through the elliptical flow distributor because of the peripheral path the fuel took as it was being guided in those directions from its initial reentry point through the baffle plate near the R7 location. Smaller, multiple pours onto the lower head apparently allowed greater heat transfer to the surrounding water and thereby allowed more rapid solidification of the material that became the initial insulating and protective crust on the lower head. Although computer codes are available for predicting the transfer of heat from fuel passing through water, the events on the TMI-2 lower head indicate the need for benchmarking the codes against situations such as those which apparently existed in TMI-2.

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Physical and Radiochemical Examinations of Debris from the TMI-2 Lower Head^a

By D. W. Akers and B. K. Schuetz^b

Abstract: As part of the Three Mile Island Nuclear Station Unit 2 (TMI-2) Vessel Investigation Project, sponsored by the Organization for Economic Cooperation and Development, physical, metallurgical, and radiochemical examinations were performed on samples of previously molten material that had relocated to the lower plenum of the TMI-2 reactor during the accident on March 28, 1979. This article presents the results of those examinations and some limited analyses of these results. Principal conclusions of the examinations are that the bulk lower-head debris is homogeneous and composed primarily of (U,Zr)O₂. This molten material reached temperatures greater than 2600 °C and probably reached the lower head as a liquid or slurry at temperatures below the peak temperature. A debris bed composed of particulate debris was formed above a monolithic melt that solidified on the lower head.

As part of the Vessel Investigation Project (VIP), companion samples were examined to (1) assess the physical and radiochemical properties of the debris adjacent to the vessel lower head, (2) assess the potential for interactions between the molten core materials and the lower head, and (3) provide information needed for the vessel margin-to-failure analysis effort.

This section summarizes results of the physical and radiochemical examinations of the companion samples and the analysis of these data. A more detailed description of companion sample examination results may be found in Ref. 1. This article also describes how the companion samples were acquired from the vessel lower head, their approximate location in the debris bed, and sample designations. The results are presented from examinations to characterize the physical characteristics of the companion sample debris and from examinations to determine radiochemical properties of the debris. Companion sample data are summarized for the

margin-to-failure analyses. Last, major conclusions from the companion sample examinations are presented.

SAMPLE ACQUISITION

As part of the defueling efforts, all loose debris on the vessel lower head was removed, revealing a variable topography of solidified debris (the companion material). Results from probing examinations performed on February 15, 1989 (see Ref. 2), were used to create the topographical map of the debris height shown in Fig. 1. The contour lines in Fig. 1 represent the depth of the hard debris (i.e., the difference between the "hard stop" from the probe tests and the bowl-shaped lower head) rather than the surface contour of the hard layer. Figures 2 and 3 illustrate cross-sectional views through this hard layer at row 10 and row 12. As indicated in Fig. 1, the maximum depth of this hard layer was approximately 46 cm and

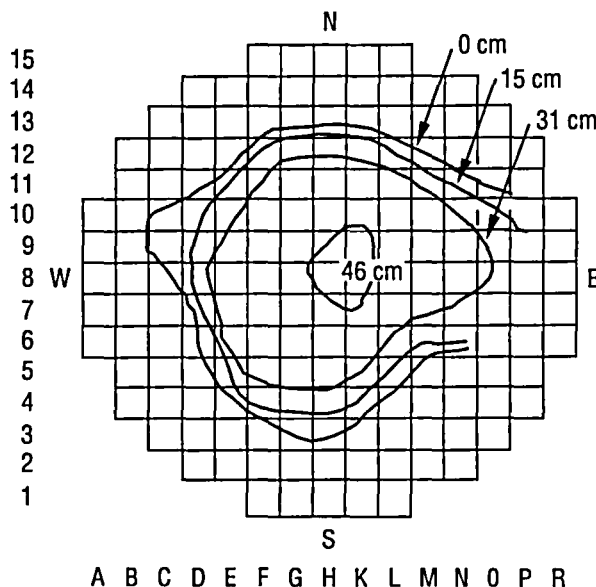


Fig. 1 Depth of hard layer of solidified debris. (Contour lines designate the distance between a "hard stop" from probe tests and the bowl-shaped lower head.)²

^aThis work was supported by the U.S. Nuclear Regulatory Commission in conjunction with the Organization for Economic Cooperation and Development through DOE Contract DE-AC07-76ID01570.

^bIdaho National Engineering Laboratory, EG&G Idaho, Inc., P.O. Box 1625, Idaho Falls, ID 83415-3840.

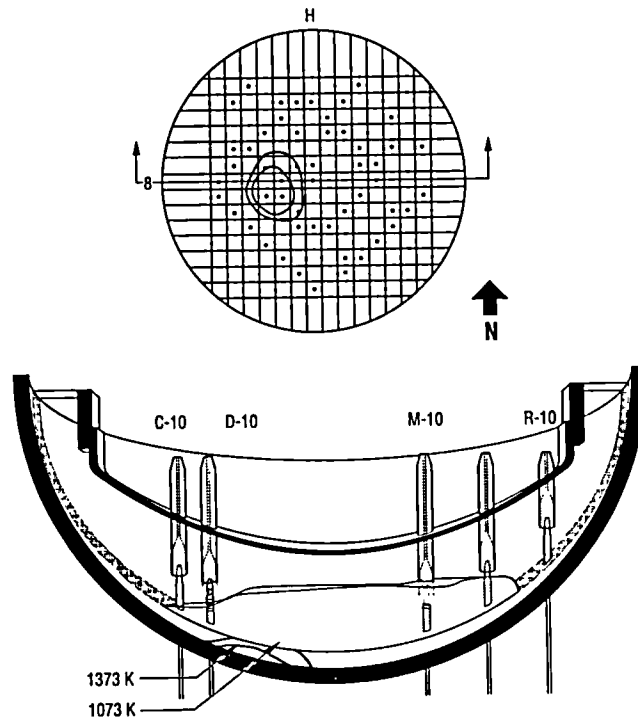


Fig. 2 TMI-2 lower-head cross section of hard debris, row 10. (In the top figure, the center of the vessel is at row 8, and the cross section shown below is highlighted.)

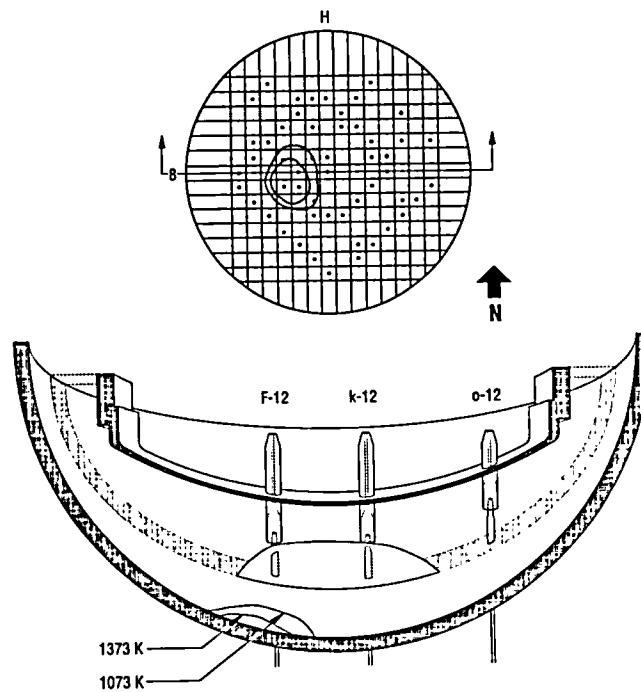


Fig. 3 TMI-2 lower-head cross section of hard debris, row 12.

was located within the central region of the core, near locations K-8 through K-10.

During the defueling process, it was discovered that the solidified layer was hard and monolithic (i.e., it could not be broken with normal defueling tools). This solidified layer was broken by a 136-kg (300-lb) slid hammer, which was dropped from an elevation of 6.1 m (20 ft). However, once the material was broken into pieces, there was virtually no adherence of the material to the lower head itself. Furthermore, the resulting pieces of debris appeared fairly uniform in composition (no metallic layer was observed).

As shown in Fig. 4, bulk companion samples were acquired from each of the four quadrants of the reactor vessel and are designated according to the quadrant from which they were taken: 1-9 for samples from the southeast quadrant, 1-10 for samples from the northwest quadrant, 1-11 for samples from the southwest quadrant, and 1-12 for samples from the northeast quadrant. Individual pieces of samples from each quadrant were further designated by a letter. For example, samples 1-11-C and 1-11-D both come from the southwest quadrant. Unfortunately, because the samples were removed during the bulk defueling process, it was impossible to determine the exact depth from which the samples were removed. As indicated in Table 1, much less debris was obtained

Table 1 TMI-2 Bulk Sample Weights and Densities

Sample No.	Location (quadrant)	Weight, g	Density, g/cm ³
1-9	Southeast	2436	9.4
1-10	Northwest	0.5	6.9
1-11	Southwest	1214	8.6
1-12	Northeast	2700	8.2

from the northwest quadrant of the reactor vessel. During the removal of the loose layer in the northwest quadrant, almost all the hard layer was also removed. This left little debris still attached to the lower head when the companion samples were gathered. Hence examinations focused primarily on samples from the southeast, southwest, and northeast quadrants.

PHYSICAL CHARACTERIZATION

Nondestructive examinations of the companion samples included visual examinations, photography, sample weights, bulk density, and individual particle densities. Figures 5 to 8 show the bulk companion samples from which individual particles were selected for examination. All companion samples were composed of large pieces of broken-up debris except companion sample 1-10 (see Fig. 6) from the northwest quadrant. This sample was composed of fine particulate debris and was not considered to be representative of the companion sample material. In retrospect, it is suspected that sample 1-10 was material that did not get removed during attempts to remove loose debris.

Eleven individual particle samples from the lower plenum were selected for destructive examinations. The examinations included optical metallography, scanning electron microscopy (SEM) with energy-dispersive and wavelength-dispersive X-ray spectroscopy, bulk elemental analysis, and radionuclide content. Of the 11 samples, 5 were from the southeast quadrant of the reactor vessel (samples 1-9-A, 1-9-B, 1-9-C, 1-9-F, and 1-9-G). Three samples (1-11-R, 1-11-C, and 1-11-D) were from the principal damage region in the southwest quadrant of the reactor vessel, and the remaining three samples (1-12-R, 1-12-C, and 1-12-D) were from the northeast quadrant of the reactor vessel head (see Figs. 2 and 3). These samples were sectioned and prepared for metallographic examination, after which representative samples were obtained for SEM/microprobe examinations and radiochemical analysis.

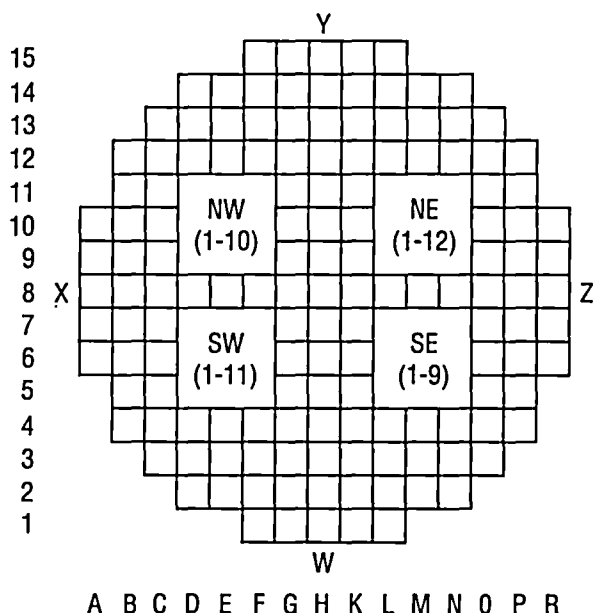


Fig. 4 Locations from which companion samples were taken. (Numbers are sample identification designations given by MPR Associates, Inc.)

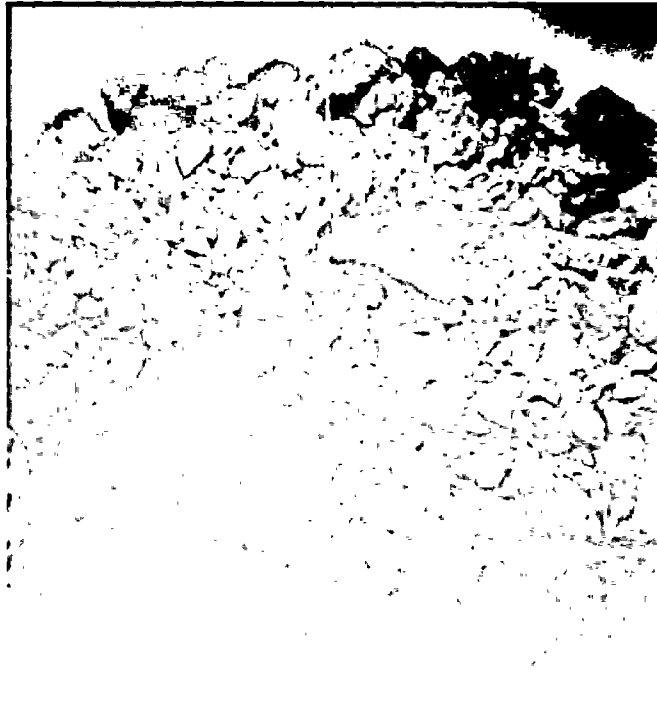


Fig. 5 Sample collected from the southeast quadrant (sample 1-9; total sample weight is 2 436 g).

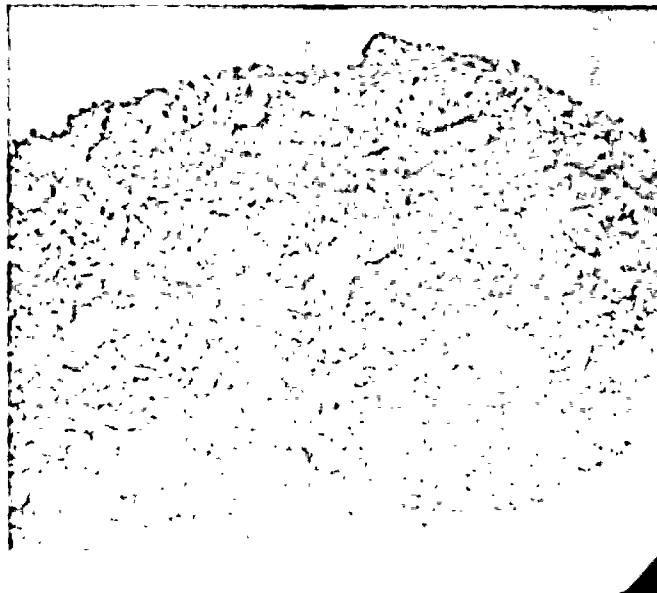


Fig. 6 Sample collected from the northwest quadrant (sample 1-10; total sample weight is 0.5 g).

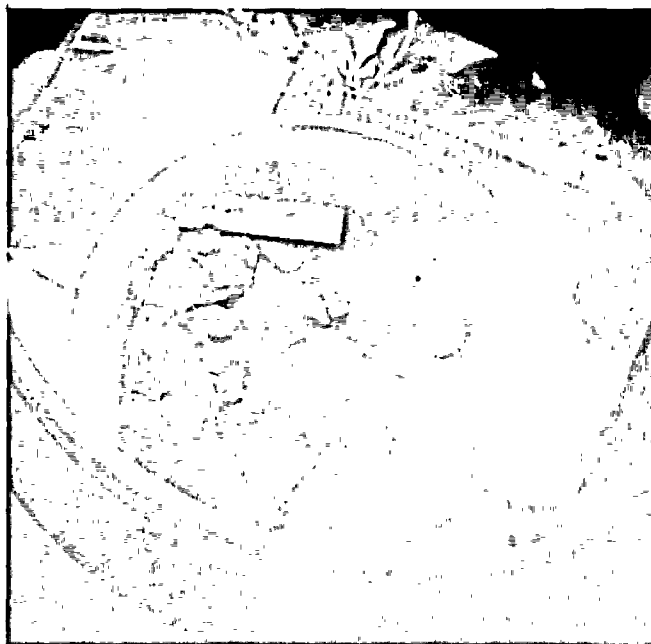


Fig. 7 Sample collected from the southwest quadrant (sample 1-11; total sample weight is 1 214 g).



Fig. 8 Sample collected from the northeast quadrant (sample 1-12; total sample weight is 2 700 g).

Visual Examinations

On the basis of previous sample examinations, visual examinations suggested that the samples were composed primarily of previously molten ceramic material and possibly included small amounts of metallic material. The samples were generally dull grey, although some areas were yellow (lighter areas in Figs. 5 to 8). This material is probably hexavalent uranium, although no analyses were performed to confirm this.

Density Measurements

Density measurements were performed on entire companion samples from each quadrant and from individual pieces of companion samples from each quadrant using the standard immersion method. Table 1 lists the location, total weight, and density of the total companion sample from each quadrant. Densities ranged from 6.0 to 9.4 g/cm³. A numerical average density for the companion samples is 8.7 ± 0.4 g/cm³. The low density of the sample taken from the northwest quadrant was excluded from this average because of the small size of the sample and its noticeable difference in physical form. Table 2 shows the weight and density of individual particles from several quadrants. Densities of these samples ranged from 7.45 to 9.40 g/cm³, with an average value of 8.4 ± 0.6 g/cm³. The measured densities are consistent with samples composed primarily of (U,Zr)O₂ with a large proportion of UO₂. Examination of the elemental analysis results indicates that the composition of all samples is similar. Hence differences in sample density are primarily attributed to differences in debris porosity.

Table 2 TMI-2 Lower Plenum Individual Sample Weights and Densities

Sample No.	Weight, g	Density, g/cm ³
1-9-R	51.81	9.40
1-9-F	14.90	7.45
1-9-G	12.10	8.07
1-11-R	52.23	8.62
1-11-C	49.50	8.39
1-11-D	76.40	8.30
1-12-R	47.16	8.18
1-12-C	45.50	9.29
1-12-D	15.20	7.60

Porosity Data

Table 3 lists porosity data for individual particle samples from the three quadrants of the lower head where most of the debris was obtained. The porosity was determined with optical methods on polished metallographic specimens. The numerical average porosities of samples from the southeast, southwest, and northeast quadrants are $21 \pm 7\%$, $18 \pm 14\%$, and $17 \pm 9\%$, respectively. These data can be misleading, however, because of several high values and the range of observed porosities. The average porosity for all samples is $18 \pm 11\%$, which suggests a very broad range of porosities in the debris. The metallographic examination of these samples indicated no significant interconnected porosity.

Microstructure Examinations

Sample 1-11-R was sectioned to provide longitudinal and transverse cross sections labeled 1-11-R/L and 1-11-R/T. Figure 9 shows apparent connected pores in

Table 3 TMI-2 Lower Plenum Sample Porosities^a

Sample No.	Porosity, %	Remarks
1-9-A	29.2	Holes/cracks
1-9-B1	10.8	Holes/cracks
1-9-B2	19.5	Holes/cracks
1-9-F	27.0	Holes/halftone ^b
1-9-G	17.3	Original macro
1-11-C	7.6	Holes/halftone
1-11-D-A	20.5	Original photo
1-11-R/L	21	Fine holes not resolved
1-11-R/T	7.0	Halftone
1-11-R/T	5.7	Large holes only
1-11-D-B	47.5	Mottled stringers of metal not included in porosity estimate
1-12-R	9.5	Halftone
1-12-R	19.8	Halftone
1-12-R	22.0	Original photo
1-12-C	5.7	Stringers of metal not included in porosity calculation
1-12-D	31.7	Original photo

^aReference 1 provides additional information related to the particular photographs from which porosity measurements were made.

^bHalftones are report-quality photographs that may not contain the level of detail of the original photographs. Some smaller porosity may not be apparent from the optical analysis. Comparisons indicate that the difference in porosity between halftones and originals is 1 to 2%.



(a)

90m141 $\times 2.9$ 

(b)

90m142 $\times 2.8$

Fig. 9 Cross-sectional views of sample 1-11-R. (a) Longitudinal section (sample 1-11-R/L). (b) Transverse section (sample 1-11-R/T).

the longitudinal section of this sample. These interconnected pores were observed in many of the samples and may have been caused by the bubbling of steam or structural material vapors through the molten pool when the pool froze.³ The physical examination of the lower head and the presence of the interconnected pores in the companion samples suggest that the molten pool cooled slowly enough to allow bubble coalescence to occur.

The morphology of the material surrounding the pores was discernible only on the scanning electron microscope. As indicated in Fig. 10, SEM examinations reveal that the material surrounding the pores within samples was composed of two phases: a light, uranium-rich (U,Zr)O₂ phase and a dark, zirconium-rich (Zr,U)O₂ phase. Away from the porous regions, the single-phase regions consisted of uranium-rich (U, Zr)O₂. UO₂-ZrO₂-phase diagrams indicate that the presence of two-phase (U,Zr)O₂ and (Zr,U)O₂ structures corresponds to material that underwent a gradual cooldown rather than a rapid quench because of the time required for apparent visible phase separation to occur.

Composition Analyses

Analyses were performed for key elements in the principal components of the Three Mile Island Unit 2

(TMI-2) core. Table 4 lists the elemental composition of each of the core constituents (see Ref. 4). Through summing of the masses of each element within the core, an average composition of the TMI-2 core was estimated assuming that the core was homogeneously mixed (including the end fittings). These values are also listed in Table 4. Note that these average values include the oxygen content of the uranium but exclude the oxygen that might be present because of the oxidation of Zircaloy and structural materials.

In-depth SEM analyses were performed to characterize the composition of companion samples 1-11-R/T, 1-9-A, and 1-9-B, which appeared visibly to be representative of the debris bed. Energy-dispersive X-ray spectroscopy was performed, and dot maps were developed with wavelength-dispersive X-ray spectroscopy to assess the composition of specific phases within the samples. Dot maps were generated for the following core constituents: U, O, Zr, Ag, Al, Cd, Cr, Fe, In, Mn, Mo, Nb, Ni, Sn, and some fission products. Reference 1 includes a discussion of the regions examined and shows dot maps of the elements for which significant results were obtained.

Areas of interest that were examined include the edge of large pores, metallic inclusions or ingots, secondary

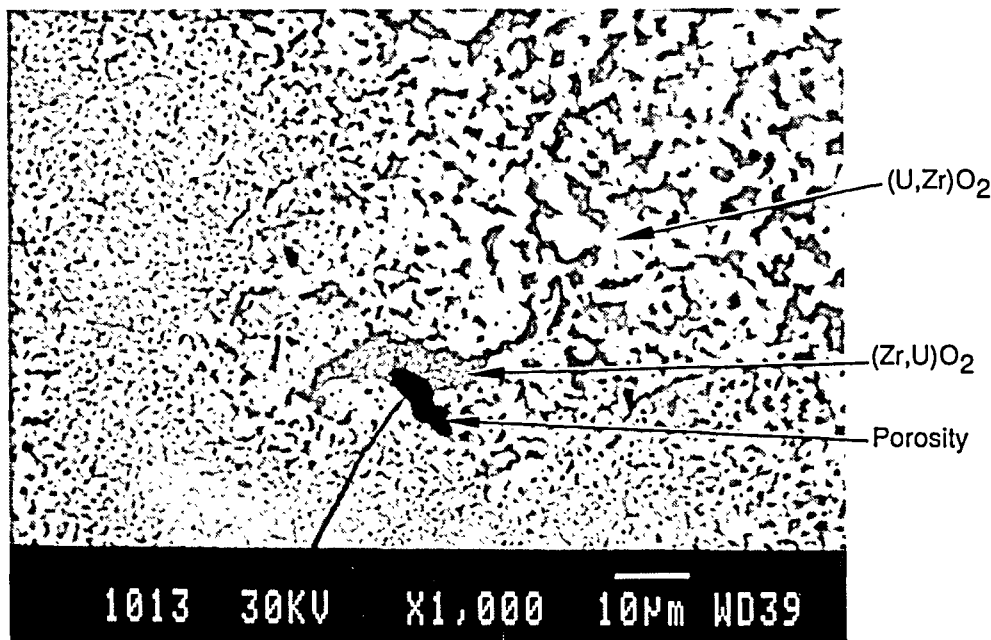


Fig. 10 SEM backscattered electron image of two-phase region (sample 1-9-A, Area 2).

Table 4 TMI-2 Reactor Core Composition^a

Material weight	Elements	Wt%	Average core composition	
			Element	Composition, wt%
UO ₂ (94 029 kg) (531.9 kg) ^b	U-235 ^a	2.265	U	65.8
	U-238 ^a	85.882	Zr	18.0
	O	11.853	O	8.5
			Fe	3.0
Zircaloy-4 (23 177 kg) (125 kg) ^b	Zr ^a	97.907	Ag	1.8
	Sn ^a	1.60	Cr	1.0
	Fe ^a	0.225	Ni	0.9
	Cr ^a	0.125	In	0.3
	O	0.095	Sn	0.3
Type 304 stainless steel (676 kg) and unidentified stainless steel (3960 kg) (16.8 kg) ^b			Al	0.2
	Fe ^a	68.635	B	0.1
	Cr ^a	19.000	Cd	0.1
	Ni ^a	9.000	Mn	0.8
	Mn ^a	2.000	Nb	0.04
	Si ^a	1.000		
	N	0.130		
	C	0.080		
	Co	0.080		
Inconel-718 (1211 kg) (6.8 kg) ^b	Ni ^a	51.900		
	Cr ^a	19.000		
	Fe ^a	18.000		
	Nb ^a	5.553		
	Mo ^a	3.000		
	Ti	0.800		
	Al ^a	0.600		
	Co	0.470		
	Si ^a	0.200		
	Mn ^a	0.200		
	N	0.130		
	Cu	0.100		
Ag-In-Cd (2749 kg) (43.6 kg) ^b	Ag ^a	80.0		
	In ^a	15.0		
	Cd ^a	5.0		
B ₄ C-Al ₂ O ₃ (626 kg) (0 kg) ^b	Al ^a	34.33 ^c		
	O	30.53 ^c		
	B ^a	27.50 ^a		
	C	7.64 ^c		
Gd ₂ O ₃ -UO ₂ (131.5 kg) (0 kg) ^b	Gd ^a	10.27 ^c		
	U ^a	77.72 ^c		
	O	12.01 ^c		

^aThese are elements for which inductively coupled plasma analysis was performed.

^bThis value is the weight of material in a control rod fuel assembly.

^cRepresentative compositions of these components were used.

phases, and pores without secondary phases. As previously discussed, each sample is composed of a homogeneous (U,Zr)O₂ matrix with relatively low concentrations of Al, Sb, and Sn, and a zirconium-rich

secondary phase around pores and at grain boundaries.^a Results from these examinations indicate that all the samples appear to consist primarily of previously molten (U,Zr)O₂. Droplets of metallic melt were found only in samples 1-11-R/L, 1-11-R/T, and 1-11-D-A (see Fig. 11). SEM/microprobe examinations indicate that these metallic melts are silver and indium. A secondary ceramic phase was also observed within the (U,Zr)O₂ matrix of sample 1-11-R/T (see Fig. 11). SEM/microprobe examinations of this ceramic phase indicate that it was composed primarily of chromium oxide.

Examination of the secondary phases around pores and in the matrix of the debris indicates that the secondary phases are composed primarily of (Zr,U)O₂ with greater amounts of iron and chromium present. The fact that there was time during the cooling process for the lower-temperature (Zr,U)O₂ phase to form and time for the iron and chromium to migrate to the secondary phases suggests that the molten pool remained at a relatively high temperature for a period of time.

RADIOCHEMICAL CHARACTERIZATION

Radiochemical analyses were performed on the companion samples to assess bulk composition and radionuclide content. Before the destructive analysis, the intact samples were analyzed through gamma spectroscopy to provide an initial estimate of the gamma-emitting radionuclide content. Then the samples were dissolved through the use of a pyrosulfate fusion technique in a closed system. Elemental analyses were performed on dissolved samples with the use of inductively coupled plasma spectroscopy techniques. Reference 5 contains a detailed description of the analysis methods used for the companion sample examinations.

Elemental Composition

Elemental analyses were performed for key elements in principal core components (see Table 4). Table 5 lists the average compositions of the companion samples from the three quadrants of the lower head for which examinations were performed. The average composition for each of the core constituents is repeated in Table 5 for comparison. Examination results indicate that the companion

^aAluminum is found in Inconel-718 that is used in spacer grid strips, tin is contained within Zircaloy that is found in fuel cladding and in other fuel assembly components, and Antimony-125 is a fission product from U-235 (see Table 4).

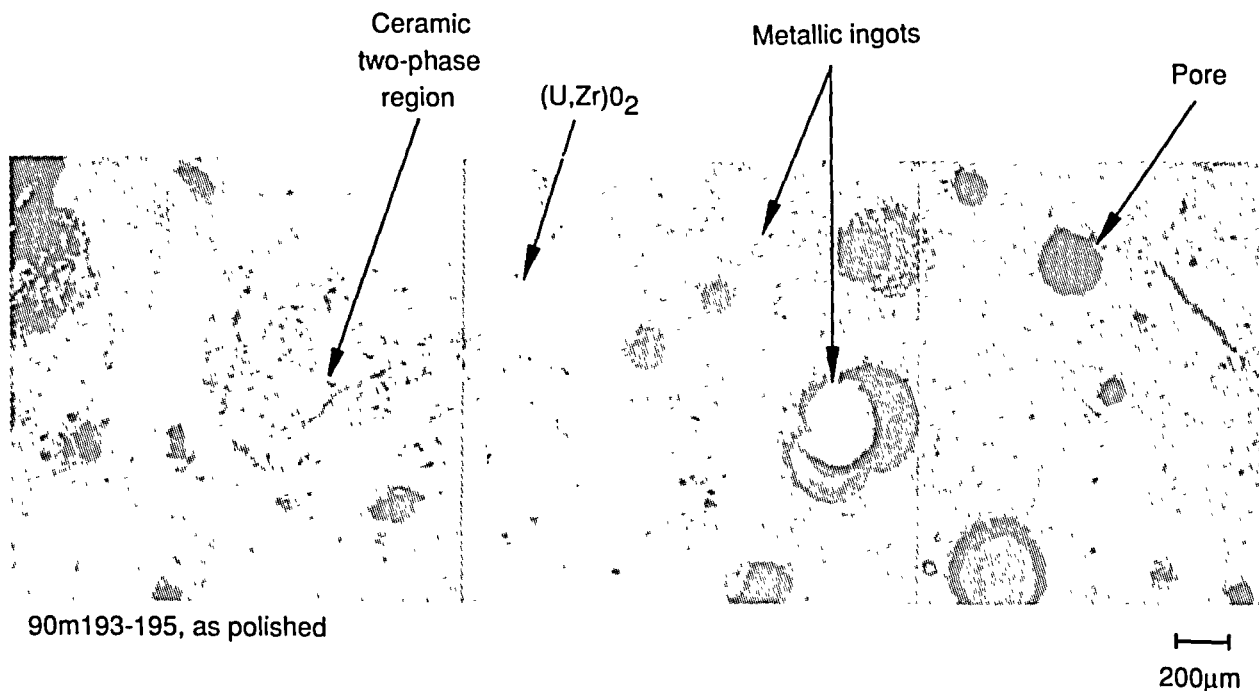


Fig. 11 Photocomposite of microstructure in sample 1-11-R/T.

material was relatively homogeneous on a macroscopic scale in all the areas examined.

The total amount of sample weight accounted for in this analysis is between 84 and 88 wt% of the total sample weight. Within the uncertainties of the analysis, the remaining material is accounted for by the oxidation of the uranium and zirconium present in the samples.

Comparison of the analysis results with the average composition of core constituents indicates that the fuel melt is composed almost entirely of the constituents of a fuel rod and that little contamination by other structural constituents occurred. It is interesting to note that a relatively high fraction of the indium within a fuel element was found in the companion samples.

Radionuclide Concentration and Decay Heat

The companion sample examination effort included analyses to determine the decay heat within the debris, which was required as input to the margin-to-failure calculational effort. The procedure used to determine the decay heat required that the radionuclide concentration within the debris be measured for selected species. These measured concentrations were compared with

concentrations predicted in an ORIGEN2 calculation^{6,7} to verify calculational results. Then other radionuclide concentrations contributing to the decay heat were obtained from the ORIGEN2 calculation, and calculations were performed to estimate the decay heat from the radionuclide concentration within the debris as a function of time. Results from major steps in this process to estimate the decay heat are presented in the following paragraphs.

Radionuclide Concentration. Dissolution techniques were used to measure the radionuclide content of the lower-head debris samples for several key radionuclides. Table 6 summarizes the normalized radionuclide retentions found in the companion samples. Radionuclide retention percentages reported in Table 6 are the ratios of measured retention to the retention predicted by an ORIGEN2 analysis for undamaged fuel.⁷ A ratio of less than 1 indicates that the measured retention is less than the calculated value. Results are discussed here according to the volatility of the chemical group and element.

The high-volatility fission-product groups include the noble gases, halogens, alkali metals, and heavy chalcogens. From this group, measurements were made for ¹³⁷Cs. As indicated in Table 6, the volatile ¹³⁷Cs was

Table 5 Average Debris Composition by Quadrant^a (wt %)

Element	Southeast (1-9)	Southwest (1-11)	Northeast (1-12)	Core average ^b
U	72.3	70.8	68.2	65.8
Zr	14.1	12.0	15.2	18.0
Sn	c	c	c	0.3
Ag	c	c	c	1.8
In	0.28	0.26	c	0.3
Al	c	c	c	0.2
Cr	0.33	0.26	0.52	1.0
Fe	0.74	0.53	0.93	3.0
Mn	0.030	0.026	0.028	0.8
Mo	c	c	c	^d
Nb	c	c	c	0.04
Ni	0.099	0.081	0.10	0.9
Total	87.8 ^c	84.3 ^c	85.1 ^c	92.14

^aThis table presents the average of the examination results obtained from the companion samples; however, because of the small number of samples examined, these data must be used with caution.

^bThis value is based on data in Table 4.

^cValues are below the analytical detection limit. Detection limits differ for individual elements; however, a nominal value is approximately 0.1 wt%. The sample matrix may affect detection limits.

^dData were not available.

^eThis value is the total of measurable constituents. Oxygen content was not measured.

Table 6 Radionuclide Retention in the Debris Bed^a

Radionuclide	Radionuclide retention, %		
	Southeast (1-9)	Southwest (1-11)	Northeast (1-12)
⁹⁰ Sr	48	47	96
¹²⁵ Sb	1.9	1.1	5.6
¹³⁷ Cs	3.6	1.3	18
¹⁴⁴ Ce	91	85	97
¹⁵⁴ Eu	83	84	80

^aRetention is calculated on the basis of the uranium content of the sample material as determined from the elemental analysis results. Results have been corrected for burnup and show a reduction of almost a factor of 2 in the inventory of ¹⁵⁴Eu and ¹²⁵Sb. Radionuclide concentration data are in Ref. 1.

measurable in all samples at retentions substantially lower than those predicted with ORIGEN2 for undamaged fuel. However, higher retentions (18%) were found in the northeast quadrant. It is not known why higher levels of this radionuclide, as well as medium- and low-volatile radionuclide concentrations, existed in the northeast region.

The medium-volatility fission products are from the metals, alkaline earths, some of the rare earths, and actinides. Radionuclides from these groups for which measurements were made are ¹²⁵Sb and ⁹⁰Sr. Strontium-90 is less volatile than ¹²⁵Sb, as discussed in Refs. 1 and 5, and is expected to be retained by the fuel to the greatest extent. However, the ⁹⁰Sr data shown in Table 6 range in retention from 48 to 96%, which indicates that this radionuclide was mobile and was not fixed in the fuel melt with the low-volatile radionuclides. The low retention of ¹²⁵Sb in the companion samples probably resulted from the partition of metallic antimony (unoxidized because of the high potential required to oxidize the element) from the oxidic uranium melt in the upper core region. As a consequence, the melt that relocated to the lower head was low in ¹²⁵Sb content. In previous core examinations, high concentrations of ¹²⁵Sb were found in metallic samples from the upper core region.⁵

The low-volatility fission products include elements from the noble metals, the remaining rare earths and actinides, tetravalents, and early transition elements. The radionuclides from this group that were measured are ¹⁵⁴Eu and ¹⁴⁴Ce. The concentrations of ¹⁴⁴Ce measured in the companion samples indicate that nearly all this radionuclide was retained. Considering the uncertainty in the ability to predict ¹⁵⁴Eu production, which for TMI-2 was verified through a burnup analysis, the data in Table 6 also indicate that most of this radionuclide was retained.

Decay Heat. Decay heat calculations were performed to estimate the heat generated within the hard layer of debris upon the lower head. Results from an ORIGEN2 analysis of the TMI-2 core were used to perform these calculations. An analysis model with 1239 fuel nodes was used to calculate burnup for the TMI-2 reactor core.⁷ Results indicate that the burnup ranged from about 900 to 6000 MWd/MtU, and the core average was about 3200 MWd/MtU. A benchmark comparison was performed with the measured ¹⁴⁴Ce concentrations (an indicator of burnup) to determine the actual burnup of the debris on the lower head. This comparison indicated that the debris was at near-average burnup. The TMI-2 reactor core was operated for approximately 96 effective full-power days.

Although the average burnup of the TMI-2 core at the time of the accident was relatively low, previous calculations⁸ indicate that the decay heat for a core that had been operated for a considerably longer period of time would not be significantly different for the time periods of concern during the reactor accident. As shown in Fig. 12, the difference in decay heat for a full burnup equilibrium core at 34 GWd/MtU and the decay heat for the TMI-2 core with an average burnup of 3.2 GWd/MtU is negligible for the first 1000 minutes after reactor scram. Although more volatile fission products would be present in higher burnup fuel than TMI-2, additional calculations⁹ that include the effect of volatile release on debris decay heat indicate that the maximum increase in fission-product decay power for relocated fuel in a full burnup core would be less than 20% for time periods of concern during the reactor accident.

With the use of the methodology described in Ref. 10, radionuclide concentrations for other species contributing to debris decay heat were estimated with results from the ORIGEN2 TMI-2 calculation. On the basis of the radionuclide concentration results discussed previously, it was determined that some principal radionuclides should not be included in decay heat calculations. Specifically, the noble gases (primarily xenon and krypton) and the high volatiles (all cesiums and iodines) were removed from the decay heat calculations. These radionuclides were omitted because they would be expected to have volatilized and been released from the fuel before the molten material relocated to the lower head.

Representative specific decay heats were calculated at 224 minutes and at 600 minutes, which is representative of the later cooldown period. The decay heat produced

from the selected radionuclide list is 0.13 W/g of debris at 224 minutes and 0.096 W/g of debris at 600 minutes after the accident. These data indicate that the decay heat production during any reactor transient in which the volatile radionuclides were released would be similar to that of TMI-2.

INPUT TO MARGIN-TO-FAILURE ANALYSIS

One of the objectives of the companion sample examinations was to obtain input for the margin-to-failure analyses. In some cases, companion sample data can be used directly as input to the margin-to-failure calculations; in other cases, additional information was required to obtain the desired margin-to-failure analysis input. This section summarizes results from the companion sample examinations that provide input to the margin-to-failure analysis effort.

Debris Composition

Radiochemical examination results indicated that the composition of the debris bed is similar for all samples with an average composition of approximately 70 wt% uranium, 13.75 wt% zirconium, and 13 wt% oxygen. This composition accounts for about 97 wt% of the debris.

On the basis of the metallography and SEM examination results, the extent of the oxidation of the companion samples can be considered to be almost complete with little or no unoxidized material present other than small quantities of materials that do not readily oxidize, such as silver.

Peak Debris Temperature at Relocation

Hofmann¹¹ addressed the range of temperatures that might be expected in a severe reactor accident and has shown that the lowest temperatures that might be expected in the dissolution of uranium by zirconium are on the order of 1760 °C, which is approximately 1000 °C below the melting point of UO₂ (approximately 2850 °C). However, the companion samples have compositions that are principally (U,Zr)O₂ (i.e., about 78 wt% UO₂ and 17 wt% ZrO₂) with some secondary (Zr,U)O₂ phases. Hofmann indicates that a well-mixed (U,Zr)O₂ solid solution, as shown by the metallography and SEM results, would be expected to be found in a peak temperature range between 2600 and 2850 °C. Consequently it is suggested that the peak temperature of the melt that relocated to the lower head was at least 2600 °C.

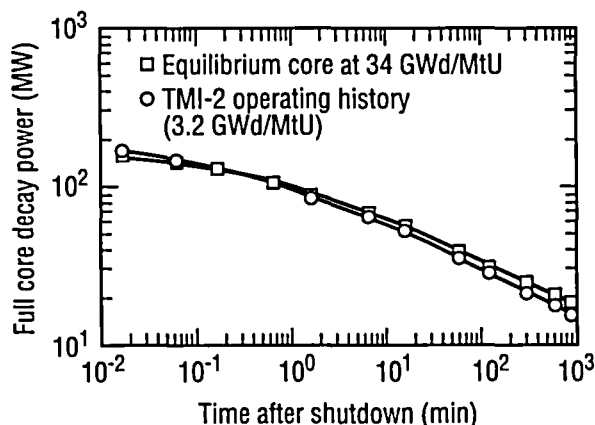


Fig. 12 Comparison of TMI-2 reactor core decay heat with a higher burnup at the core.

Debris Cooling Rate

Companion sample examinations provide insight into the debris cooling rate, which is based on the formation of secondary phases around pores and in the matrix material. These secondary phases contain apparent $(\text{Zr,U})\text{O}_2$ phases with the presence of iron and chromium. The formation of these phases would require a finite cooldown period as opposed to an instantaneous quench to allow the phase separation to occur between the $(\text{U,Zr})\text{O}_2$ and $(\text{Zr,U})\text{O}_2$ phases. Bart¹² has suggested that a cooling time between 3 and 72 hours is needed to cause this type of phase separation.

Debris Decay Heat

On the basis of radionuclide concentrations measured within the companion sample debris, it is estimated that the decay heat within the debris at 224 minutes after shutdown is 0.18 W/g of uranium; and at 600 minutes after shutdown, it is 0.14 W/g of uranium. After conversion of these data to the known debris composition, the decay heat present is 0.13 W/g of debris at 244 minutes and 0.096 W/g of debris at 600 minutes.

SUMMARY AND CONCLUSIONS

Examinations were performed on samples from the hard, monolithic layer of debris near the lower head, which are referred to as companion samples. These examinations indicate that the companion samples were relatively homogeneous with relatively small variations in composition and density. The companion samples consisted primarily of previously molten $(\text{U,Zr})\text{O}_2$ ceramic melt. Small amounts of metallic melt (<0.5%) were observed only in samples from the southwest quadrant.

The pores in some of the samples were interconnected and surrounded by microporosity and two-phase structures consisting of uranium-rich $(\text{U,Zr})\text{O}_2$ and zirconium-rich $(\text{Zr,U})\text{O}_2$. This interconnected porosity may result from gases percolating up through the melt, which suggests that the debris was molten while on the lower head and it remained molten for a sufficient period of time to allow bubble coalescence. The presence of two-phase $(\text{U,Zr})\text{O}_2$ and $(\text{Zr,U})\text{O}_2$ structures in areas of some samples indicates that these specimens were not rapidly quenched. However, the incomplete phase separation in these samples suggests that these specimens were not at high temperatures for an extended period of time.

Radiochemical analyses of the debris indicate that the debris was composed of approximately 70 wt% uranium,

13.75 wt% zirconium, and 13 wt% oxygen. This composition accounts for approximately 97 wt% of the debris. The remaining constituents are the elemental constituents of stainless steel and Inconel core components that probably melted during the relocation of debris to the lower head.

The examinations suggest that much of the high-volatile radionuclide content had volatilized out of the debris before the solidification of the molten debris and thus left primarily medium- and low-volatile components in the debris bed. The small amount of interconnected porosity and the nonreactive nature of the solidified ceramic indicate that leaching and other release mechanisms were insignificant. Decay heat analyses were performed to determine the amount of heat present in the debris bed at the time of relocation (224 minutes after shutdown) and at 600 minutes after reactor shutdown. Calculation results indicate that the retained heat in the lower debris bed was approximately 0.13 W/g of debris at 244 minutes after shutdown and 0.096 W/g of debris at 600 minutes after shutdown.

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Results of Metallographic Examinations and Mechanical Tests of Pressure Vessel Samples from the TMI-2 Lower Head^a

By D. R. Diercks^b and G. E. Korth^c

Abstract: Fifteen prism-shaped steel samples were removed from the lower head of the damaged Three Mile Island Nuclear Station Unit 2 (TMI-2) reactor pressure vessel to assess the effects of approximately 19 metric tons of molten core debris that had relocated there during the 1979 loss-of-coolant accident. Metallographic examinations of the samples revealed that inside-surface temperatures of 800 to 1100 °C were attained during the accident in an elliptical "hot spot" approximately 1 × 0.8 m. Tensile, creep, and Charpy V-notch specimens were cut from the samples to assess the mechanical properties of the lower-head material at temperatures up to the peak accident temperature. These properties were used in a margin-to-failure analysis of the lower head.

The Three Mile Island Nuclear Station Unit 2 (TMI-2) Vessel Investigation Project (VIP) is an international program conducted jointly by the U.S. Nuclear Regulatory Commission (NRC) and the Organization for Economic Cooperation and Development/Nuclear Energy Agency (OECD/NEA). The objectives of the overall program are to (1) determine a scenario for the relocation of molten core debris during the TMI-2 nuclear reactor loss-of-coolant accident in March 1979 and deduce the thermal history of the steel in the lower vessel head during the relocation event, (2) determine the mechanical properties of the lower head steel under the accident conditions, and (3) assess the integrity of the TMI-2 lower head under the accident conditions. Participants in the project include the United States, Japan, Belgium, Germany, Finland, France, Italy, Spain, Sweden, Switzerland, and the United Kingdom (U.K.).¹⁻¹⁴

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The relocation of approximately 19 000 kg of molten core debris onto the lower head of the reactor pressure vessel during the accident caused a considerable threat to the integrity of the pressure vessel. The lower head is fabricated of 136-mm-thick A 533, Grade B, Class 1 low-alloy steel base metal with a 5-mm-thick type 308L stainless steel inside cladding. The approximately 19 000 kg of molten debris had the potential to melt the lower head or cause it to fail by short-term creep under the tensile loadings present during the accident. That the lower head did not fail indicates that significant melting did not occur and that time at temperature was not sufficient to produce creep failure under the loadings that were present. The purpose of the present investigation is to determine the maximum temperature of the lower-head material during the accident and to measure the mechanical properties of that material under the accident conditions. These results were subsequently used in another phase of the TMI program to assess the integrity of the lower head during the accident.¹³

Fifteen prism-shaped samples, each approximately 152 to 178 mm long, 64 to 89 mm wide, and 64 to 76 mm deep, were recovered from the TMI-2 lower head during the first phase of the program (Fig. 1). The samples were cut from the inner surface of the lower head and typically extend through approximately half of the lower-head thickness. These 15 samples were subjected to detailed initial examinations and were then sectioned into metallographic and mechanical test specimens for further characterization (Fig. 2). The results of the initial sample examinations, metallographic studies, and mechanical tests are reported here. Results of the examinations of the lower-head samples before sectioning and of selected instrument nozzles from the lower head are reported elsewhere in Ref. 14.

METALLOGRAPHIC STUDIES

Following initial examinations at Argonne National Laboratory (ANL), metallographic specimens were cut

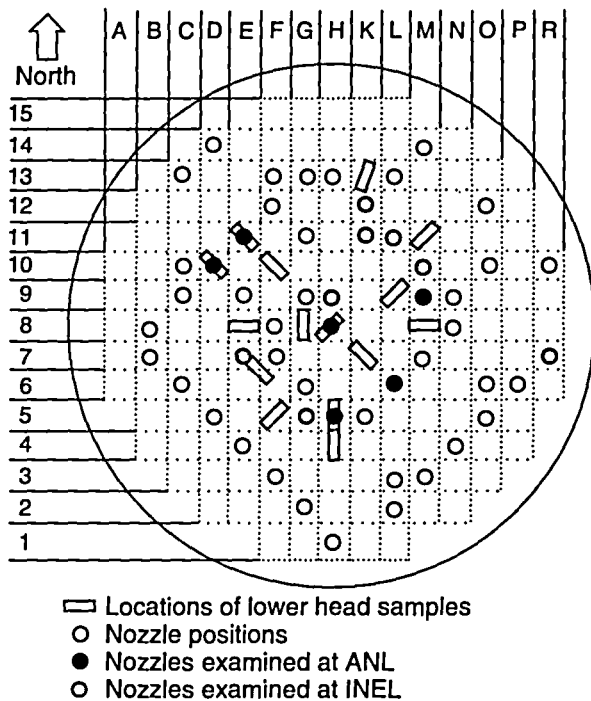


Fig. 1 Grid map of TMI core showing locations of lower-head samples and nozzles.

from the lower-head samples, decontaminated, and sent to the Idaho National Engineering Laboratory (INEL). These specimens were subjected to detailed characterization by optical metallography and hardness measurements to determine the maximum temperature attained at various lower-head locations during the accident. The ANL and participating OECD partner laboratories also conducted supplemental examinations.

Background Information

For corrosion protection, the A 533 B low-alloy steel TMI-2 lower head was clad on the inside with Type 308L austenitic stainless steel by a multiple-wire submerged arc welding process. The fabrication history of the vessel is summarized as follows: 136-mm (minimum) plate formed to shape by hot pressing, austenitized at 871 to 899 °C for 5.5 hours, brine quenched and tempered at 649 °C for 5.5 hours, clad on the inside with 5-mm-thick (minimum) ER308L stainless steel, and then stress-relieved at 607 °C for 50 hours.

Because the amount of material extracted from the TMI-2 vessel was limited, archive A 533 B steel was also obtained from the abandoned Midland reactor, which had never been put into service. The Midland reactor pressure vessel was of the same design and

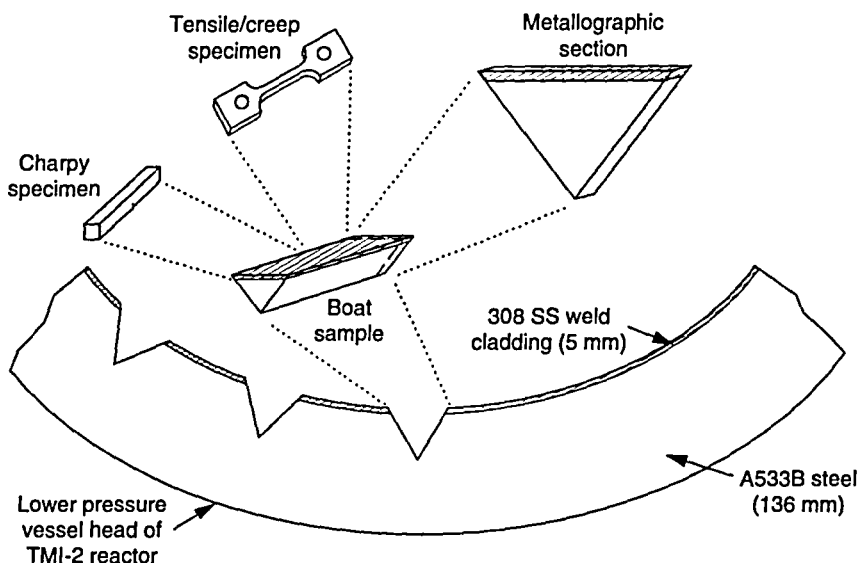


Fig. 2 Schematic diagram of source of TMI-2 metallographic and mechanical property samples.

vintage as the TMI-2 vessel, was built by the same contractor, and had a very similar fabrication history. The Midland material was plentiful and provided a valuable resource for studying properties and accident-simulated thermal response of lower-head material.

The cladding overlay and fabrication history left their "thermal signature" on the lower head. In some cases this as-fabricated condition was further altered thermally by the molten core debris that relocated during the accident. The typical as-fabricated condition (microstructure and hardness) found in the TMI-2 and Midland lower-head material is illustrated in Fig. 3 for sample H-5 from the TMI-2 lower head. A heat-affected zone (HAZ) of 7 to 12 mm is observed in the A 533 B steel directly adjacent to the stainless steel cladding. The first 2 to 3 mm of the HAZ is made up of enlarged, partially decarburized grains, and the remainder of this HAZ band consists of refined grains that reached temperatures above the ferrite-to-austenite transformation temperature of 727 °C from the welding operation and were then quenched because of the massive heat sink provided by the remaining material. Beyond the HAZ band, tempered bainite is uniformly observed throughout the remaining thickness. Any further thermal exposure greater than 727 °C during the accident would alter this as-fabricated structure and create a new thermal signature, which could be used to determine the thermal history caused by the accident.

The thermal histories of the lower-head samples were assessed by hardness profiles and microstructural examinations of the base metal, cladding, and interface regions. Typical hardness profiles were taken of the samples from the weld cladding to the bottom tip of the triangular cross sections through the lower-head samples. The microstructure was examined by standard optical metallographic practices or by scanning and transmission electron microscopy.

Hardness Measurements

The hardness profiles of most of the TMI-2 samples displayed the typical characteristic profile of as-fabricated material, as shown in Fig. 3, but the hardness profiles from samples E-6, E-8, F-10, and G-8 were markedly different from the other samples, as shown in Fig. 4. In these four samples the characteristic hardness profile through the HAZ band had risen sharply to much higher levels and was then sustained throughout the full sample depth. Heat-affected bands from the cladding were not evident in these four samples but were completely eliminated by the thermal effects of the accident.

Two other samples (H-8 and F-5) also showed anomalies in the hardness profiles. The hardness of H-8, measured in a longitudinal direction (parallel to the inside surface of the lower head) on several strips remaining after tensile specimens were cut, increased as the end closest to G-8 was approached. This observation indicates that the ferrite-to-austenite transformation temperature was reached on the end of H-8 nearest to G-8. The hardness profile of F-5, as measured by some of the participating laboratories, showed some deviation from the typical weld HAZ effects, which indicates that temperatures near this sample slightly exceeded the 727 °C threshold.

The final hardness of the TMI-2 samples not only strongly indicates that the A 533 B steel transformation temperature of 727 °C was exceeded during the accident but also indicates some bounds on the cooling rate back through the phase change. To achieve the same hardness values on standards as observed in samples E-6, E-8, F-10, and G-8, the cooling rate must have been 10 to 100 °C/min. Studies with the Midland material showed that if the cooling rate had been approximately 1 °C/min or less, the final hardness would have been approximately the same as that of the parent metal. If that had been the case, hardness measurements would not have been very helpful in determining the thermal history as a result of the accident; they would reveal only that the hardness peak from the HAZ band in the cladding was eliminated. However, the final hardness values for E-8, F-10, G-8, and E-6 are consistent with cooling rates ≥ 10 °C/min and peak temperatures above 800 °C. Therefore hardness values of the TMI-2 samples indicate (1) whether the material had exceeded the transformation temperature and (2) if it had, some bounds on the cooling rate. Hardness values are not conclusive as to the peak temperatures that may have been reached, even though some trends were observed by ANL and Saclay in France. From just the hardness measurements, it was concluded that F-5 and one end of H-8 slightly exceeded 727 °C and that E-6, E-8, F-10, and G-8 exceeded 830 °C. Examination of the microstructure, discussed in the following text, was used to assess peak temperatures after the initial screening was performed with hardness measurements.

Midland Archive Standards

Standards with known thermal histories were prepared from Midland archive material and later from actual as-fabricated TMI-2 material. These accident-simulated standards provided a means to compare a similar material, with a known thermal history, with

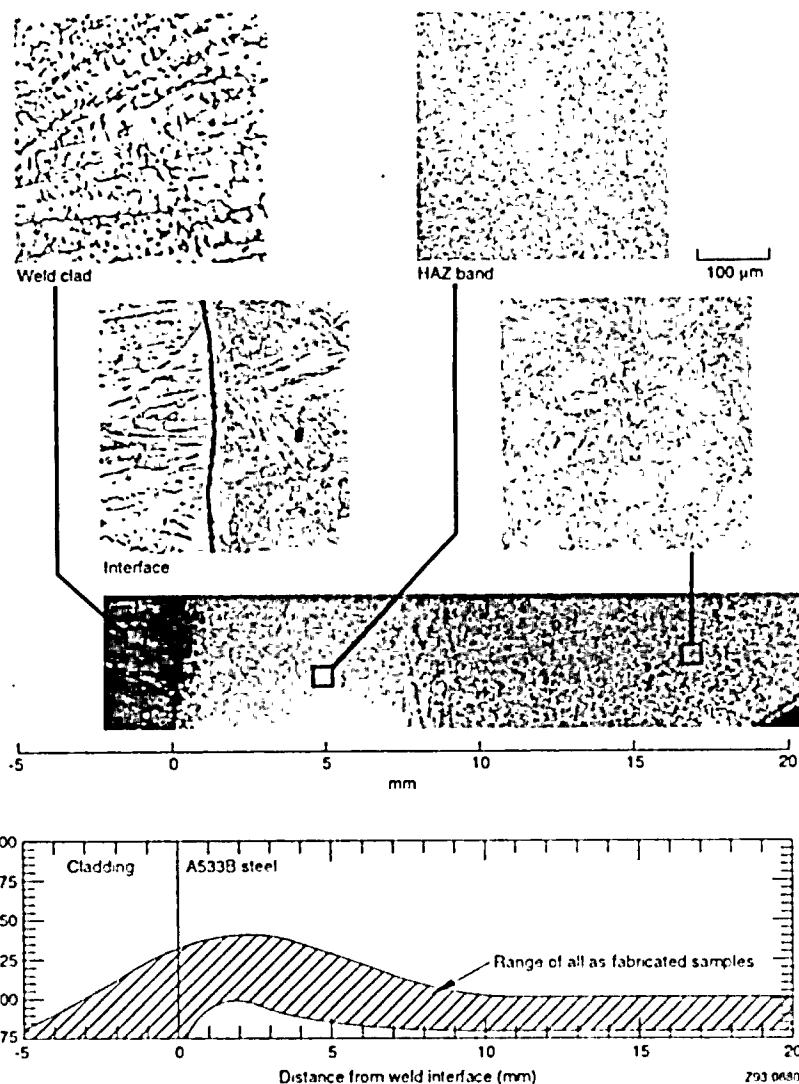


Fig. 3 Typical as-fabricated microstructure and hardness of TMI-2 lower-head material (from TMI-2 lower-head sample H-5).

TMI-2 material with an unknown thermal history. Initially, standards were prepared to determine the effect of cooling rate through the austenite-to-ferrite transition temperature range, which affects hardness. Several laboratories then prepared standards from Midland archive material with maximum temperatures that ranged from 700 to 1300 °C and with dwell times at peak temperatures of 1 minute to 2 hours. The heat-up rate was controlled at 40 °C/min, and the cooling rate following the dwell period was 1 to 100 °C/min. Finally, as unknown thermal histories were narrowed down, an

additional set of standards was prepared from actual TMI-2 lower-head material determined to be in the as-fabricated condition. These small sections of TMI-2 material were heat-treated at 950, 1000, 1050, and 1100 °C for dwell times of 10, 30, and 100 minutes and provided the final basis for comparison to determine the thermal history of the lower head as a result of the accident.

As the standards were prepared and examined, various metallurgical observations revealed a stepwise process that could be used to determine the thermal histories of the TMI-2 samples. A diagram (Fig. 5) was constructed

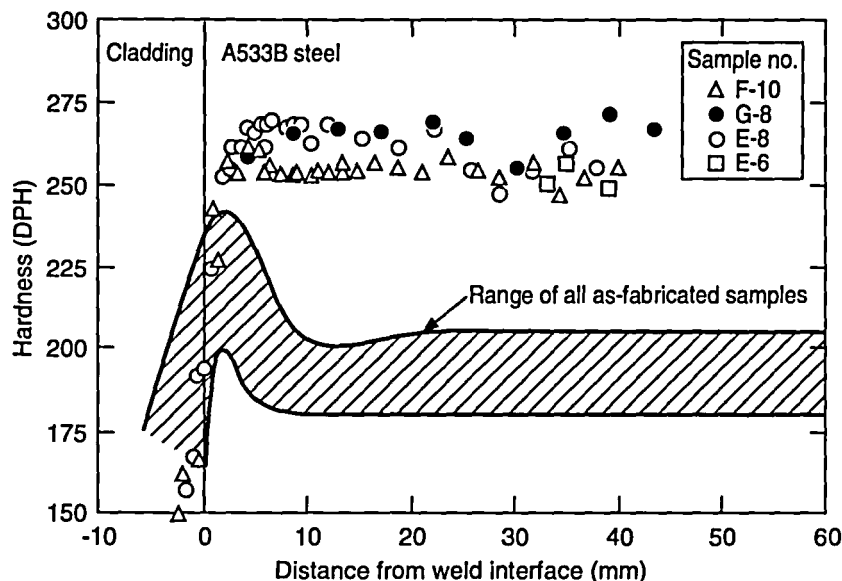


Fig. 4 Hardness profiles of samples F-10, G-8, E-8, and E-6 compared with as-fabricated samples.

to illustrate the metallurgical changes with time and temperature of the Midland and TMI-2 lower-head A 533 B steel with a 308L stainless cladding. Because the vessel was stress-relieved at 607 °C, after the cladding was in place, no thermal effects from the accident could be detected at or below this temperature; therefore the diagram shows metallurgical observations only for temperatures above this point. The lowest temperature indicator was the ferrite-to-austenite transformation, which starts at 727 °C and is complete by approximately 830 °C. Variations in the typical as-fabricated hardness profile will be evident when this threshold is exceeded. The next indicator is the dissolution or dissipation of a dark feathery band at the interface; this occurs between 800 and 925 °C, depending on time of exposure. The next indicator of increasing temperature is the appearance of small equiaxed grains, which formed in the A 533 B steel adjacent to the interface between 850 and 900 °C and disappeared between 1025 and 1100 °C as they were consumed by grain growth in the low-alloy steel. These equiaxed grains, which are not typical for a low-alloy steel, appear to be devoid of cementite, probably because of a loss of carbon into the stainless steel during the elevated temperature excursion associated with the accident. Grain growth in the A 533 B steel becomes significant above approximately 950 to 1075 °C, depending on the time involved.

The highest temperature indicator shown in Fig. 5 is the change in morphology of the δ -ferrite islands in the

stainless steel cladding. In the approximate range of 975 to 1000 °C at 100 minutes or 1100 to 1125 °C at 10 minutes, the δ -ferrite islands begin to lose their slender branch-like morphology and become spherical in shape. This spheroidizing of the δ -ferrite islands is believed to be associated with the dissolution of $M_{23}C_6$ carbides that decorate the ferrite-austenite boundaries and stabilize their shape. When the carbides dissolve, the δ -ferrite becomes more spherical to minimize surface energy. There was also evidence that some of the δ -ferrite was consumed into the austenitic matrix after exposures above 1000 °C because there was a net loss of δ -ferrite after cooling. Researchers in Germany⁷ and Spain⁹ used magnetic measurement techniques to determine that δ -ferrite levels in the cladding of nonaffected samples were 4 to 5 vol % but only 1.4 vol % in E-8.

Microstructure of TMI-2 Samples

The microstructural indicators illustrated in Fig. 5 were used to further assess the thermal history of the four samples (E-6, E-8, F-10, and G-8) that clearly show thermal effects above the ferrite-to-austenite transformation temperature. Examinations of the microstructure showed that the dark feathery band had dissipated at the A 533 B steel-weld cladding interface in all four samples. Austenitic grain growth was evident in all four samples, with E-6 and E-8 showing the most pronounced effect. Sample F-10 revealed that a small remnant of the

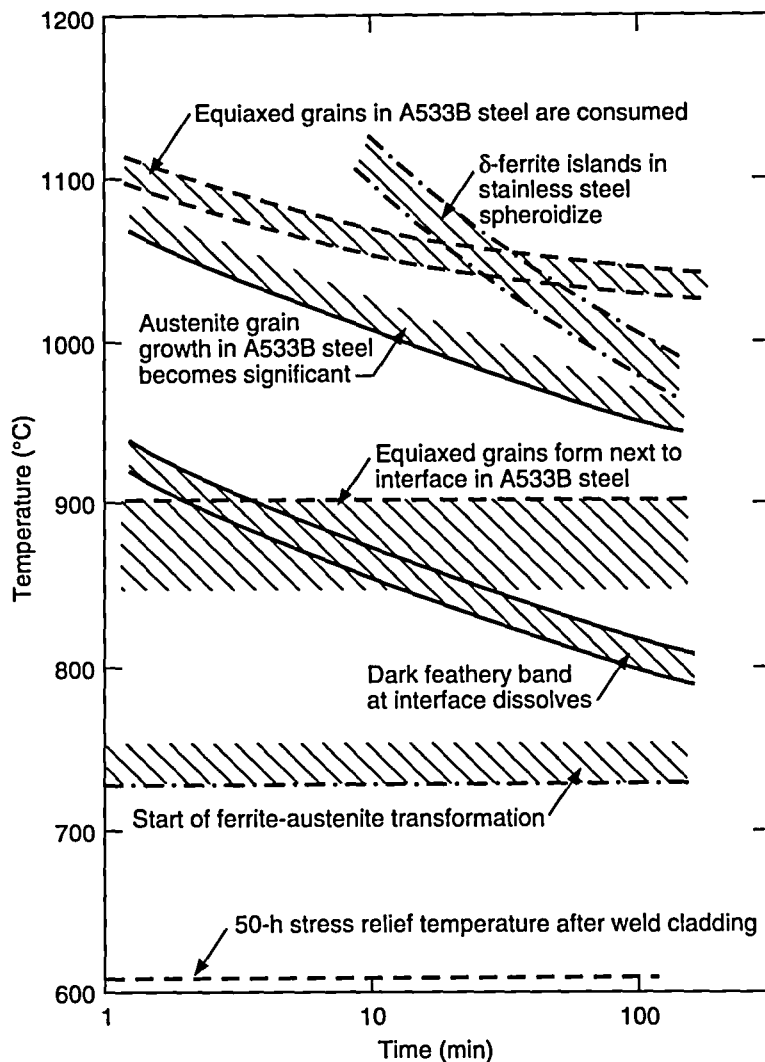


Fig. 5 Diagram of time-temperature observations of A 533, Grade B pressure-vessel steel clad with Type 308L stainless steel.

cementite-devoid equiaxed small ferrite grains was still present; none was evident in the other three samples. Spheroidization of the δ -ferrite islands in the cladding was not readily detected in F-10, was partially observed in G-8, and was fairly significant in E-6 and E-8. The preceding microstructural observations were supplemented by scanning electron microscopy of etched specimens and surface replicas and analytical transmission electron microscopy of thin foils and carbon extraction replicas.² By means of meticulous comparisons of these observations with the standards of known thermal history, INEL, the lead laboratory for metallurgical examinations, estimated peak temperatures and time at

temperature within approximately 2.5 mm of the cladding-base metal interface as follows:

- E-6 and E-8: 1075 to 1100 °C for ~30 minutes
- F-10 and G-8: 1040 to 1060 °C for ~30 minutes

The results of examinations at ANL and some of the OECD partner laboratories of different sections of these same samples are consistent with the INEL conclusions. U.K. researchers showed evidence that M-11 also slightly exceeded the 727 °C transformation temperature near the surface, although this determination was not confirmed by five other laboratories that examined different sections of the M-11 boat sample. On the basis of the preceding

observations and conclusions, a thermal contour map of peak temperatures (Fig. 6) was constructed. The hardness profile and microstructure of one of the thermally altered samples, E-8, is shown in Fig. 7.

The temperature gradient through the thickness of the lower vessel head wall was estimated by two methods. First, because the high level of hardness of the four affected samples persisted to the full depth of the boat samples (50 mm from the inside surface or 45 mm from the cladding interface; see Fig. 4), it could be concluded that the temperature at that depth was greater than the 727 °C transformation temperature. Second, on the basis of the assumption from the microstructure comparisons that the thermal excursion on the lower head as a result of the accident was approximately 30 minutes, prior austenite grain size at the bottommost tip of the heat-affected samples was compared with prepared standards that were heat-treated for 30 minutes. This comparison indicated that the temperature 50 mm from the inside surface (45 mm from the stainless steel-low-alloy steel interface) was 50 to 150 °C lower than the peak temperatures determined previously for the region near the interface. By combining temperature gradient estimates from INEL, ANL, and Finland and assuming a linear

relationship, the gradient was estimated to be 2 to 4 °C/mm.

MECHANICAL-PROPERTY TESTS

Test specimens were cut from the lower-head samples to determine the mechanical properties of this material under the accident conditions. The tests conducted included tensile tests at room temperature and 600 to 1200 °C, stress-rupture tests at 600 to 1200 °C, and impact tests over the temperature range from -20 to +300 °C. The results of these tests are summarized here; more complete information, including a tabulation of the data and the strain vs. time curves for the stress-rupture tests, can be found in Ref. 15.

The room-temperature tensile tests were conducted to obtain results that could be compared with literature data; the minimum temperature of 600 °C for the remaining tests reflects the judgment that (1) little or no damage would have occurred to those portions of the lower head for which the maximum temperature did not exceed this value and (2) failure was unlikely at these locations. The maximum temperature of 1200 °C for these tests lies slightly above the maximum lower-head temperature believed to have been attained during the accident.

The tests were conducted on specimens with various prior thermal histories that resulted from the accident. Because the number of specimens from the portion of the lower head that reached the highest temperature was limited, it was necessary, in some cases, to heat-treat low-damage specimens before testing to produce a corresponding microstructure. This treatment consisted of heating the specimen to 1000 °C, holding it at this temperature for 2 hours, and then cooling it to room temperature at about 10 to 50 °C/min. For specimens to be tested at 1000 °C or greater, this prior heat treatment was omitted because its effects would be negated by the thermal treatment imposed during testing.

Tensile Tests

The tensile tests were conducted in general accordance with American Society for Testing and Materials (ASTM) Standards E8 and E8M using a rectangular cross-section specimen that also complied with applicable standards of the Deutsches Institut für Normung. All elevated temperature tests were conducted in an argon or helium environment. The strain rate for the elastic portion of the loading was $\leq 5 \times 10^{-4} \text{ s}^{-1}$, and the strain rate during plastic loading was $4 \times 10^{-4} \text{ s}^{-1} \pm 1 \times 10^{-4} \text{ s}^{-1}$.

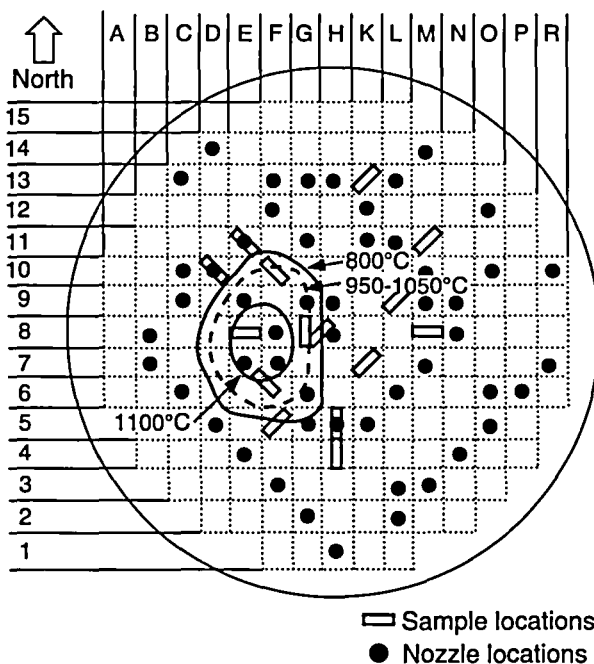


Fig. 6 Thermal contour map of peak temperature constructed as best estimate on the basis of results of metallographic examinations of boat samples.

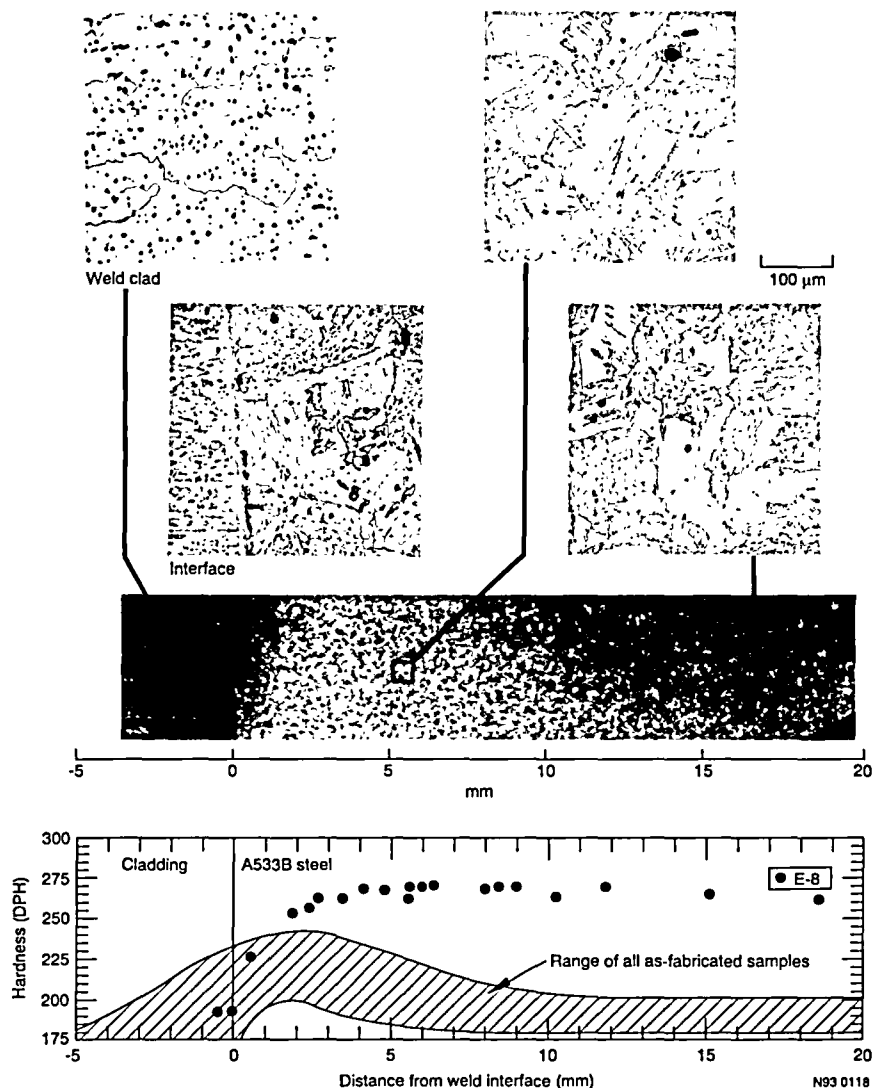


Fig. 7 Microstructure and hardness profile of sample E-8.

The reported yield strength values were obtained by the 0.2% offset method except where discontinuous yielding occurred; in these cases, the observed upper yield strength was reported.

The results of the tensile tests conducted on the lower-head base-metal specimens are shown in Fig. 8. These tests were carried out at ANL³ as well as in Belgium,⁴ France,⁶ and Spain.¹¹ Also plotted in Fig. 8 are average values reported by the Japanese National Research Institute for Metals (NRIM) for five other heats

of A 533, Grade B, Class 1 steel.¹⁶ The NRIM data were obtained at a strain rate of $5 \times 10^{-5} \text{ s}^{-1}$ up to yield and $1.25 \times 10^{-3} \text{ s}^{-1}$ for the remainder of the test. The NRIM tensile strength data suggest a strain-aging effect between 100 and 300 °C, which resulted in a local tensile strength minimum at approximately 150 °C. Both the tensile and yield strengths of this alloy are strongly temperature dependent; the room-temperature tensile strength values are reduced by a factor of more than 2 at 600 °C and more than 10 at 900 °C.

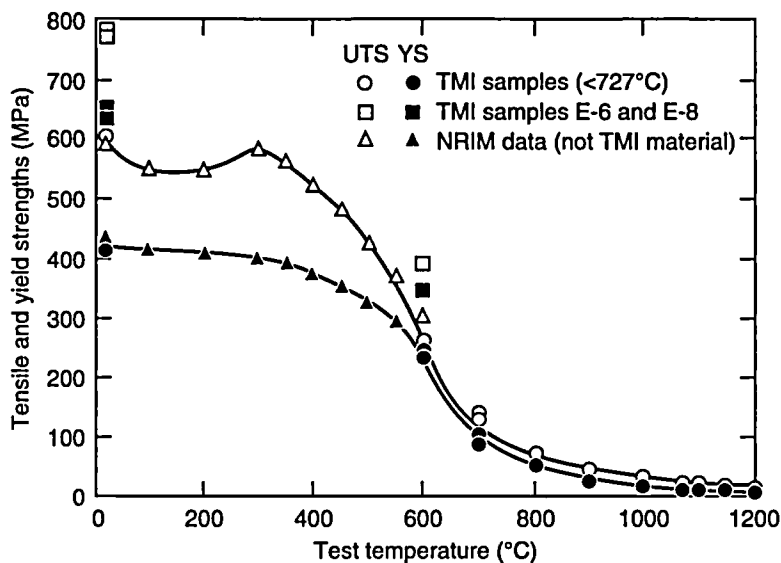


Fig. 8 Tensile and yield strengths of TMI-2 lower-head material. UTS, ultimate tensile strength; YS, yield strength.

The data for specimens taken from lower-head samples E-6 and E-8 are plotted separately in Fig. 8, and these data lie significantly above the best-fit curve to the remaining data. As discussed earlier, both of these samples were heated to maximum temperatures of about 1075 to 1100 °C during the accident, followed by a relatively rapid cooling at about 10 to 100 °C/min. The resulting hardening has produced significant increases in strength at both room temperature and 600 °C.

Stress-Rupture Tests

The stress-rupture tests used the same specimen design as the tensile tests, and testing was carried out in general accordance with ASTM Standard E139. These tests were carried out at ANL³ and in Belgium,⁴ France,⁶ and Spain.¹⁰ The resulting data for stress vs. time to failure are plotted in Fig. 9 along with a Manson-Haferd best fit (explained in the following text). The tests were conducted in an argon or helium environment except for those conducted in Belgium. All but one of the Belgian tests were conducted in a vacuum; a single test at 800 °C and 30 MPa was conducted in an argon environment.

Materials with slightly different thermal histories were tested at both 600 and 700 °C. At 600 °C, tests were conducted on specimens from sample K-13, for which

the maximum temperature during the accident did not exceed 727 °C, as well as on specimens from sample F-5, for which the maximum temperature was apparently somewhat greater than 727 °C over a portion of the sample. No significant difference in time to failure is observed in Fig. 9. This lack of an effect may be attributed to the fact that the maximum temperature probably did not significantly exceed the transformation temperature of 727 °C in F-5, particularly in the bottom half of the sample, from which the creep test specimens were taken. Similarly, at 700 °C, specimens from sample M-11, for which the maximum temperature may have approached or slightly exceeded 727 °C, show no difference in behavior when compared with specimens from sample H-8, for which the maximum temperature remained below 727 °C.

Two time-temperature correlations were explored in an attempt to fit the base-metal creep data. The first of these was the Larson-Miller parameter L (Ref. 17), defined as

$$L = T[C + \log_{10}(t_f)]$$

where T is temperature in Kelvin, t_f is time to failure in hours, and C is a fitting constant. A least-squares analysis determined that the optimal value of C for the present

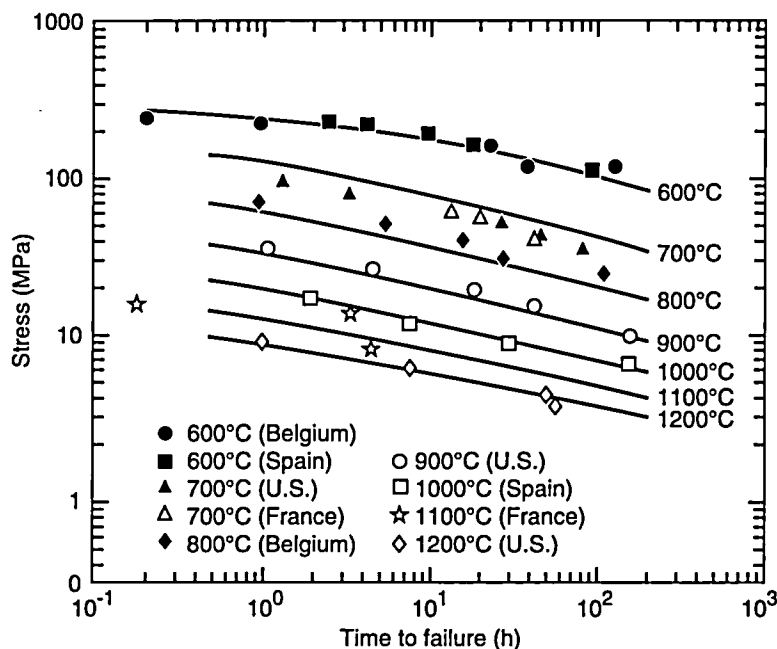


Fig. 9 Stress-rupture data with Manson-Haferd best fit.

data base was 12.5, and stress σ was related to the Larson-Miller parameter by the relation

$$\log_{10}(\sigma) = 4.3406 - 0.00018767 \cdot L \quad (1)$$

where the applied stress σ is in MPa.

The ability of the Manson-Haferd time-temperature correlation¹⁸ to fit the data was also evaluated. The Manson-Haferd parameter M has the form

$$M = \frac{\log_{10}(t_f) - t_a}{T - T_a}$$

where t_f is time to failure in helium, T is test temperature in Kelvin, and t_a and T_a are fitting constants. A least-squares analysis was again carried out, and the optimal values for t_a and T_a were found to be 7.57 and 520, respectively. $\log(\sigma)$ was found to vary with the Manson-Haferd parameter M according to the relationship

$$\log_{10}(\sigma) = -0.80467 - 261.41 \cdot M - 5291.25 \cdot M^2 \quad (2)$$

A comparison of the resulting best-fit curves with the actual σ vs. t_f data in Fig. 9 shows a reasonably good fit

to the data. However, systematic departures from the actual data are noted in the 700 to 900 °C region. This problem may be associated in part with the ferrite-to-austenite phase transformation that occurs over the temperature regime from 727 to approximately 830 °C.

Impact Tests

The impact tests on the lower-head material were conducted in Italy⁸ by using the procedure and conventional Charpy V-notch test specimen described in ASTM E23; the data are summarized in Fig. 10. The three groups of test specimens for which the maximum temperature did not exceed 727 °C (D-10, H-4, and E-11) show similar behavior, with an upper-shelf energy of approximately 170 J and a transition temperature of approximately 20 °C. However, the data from specimens of sample F-10, for which the maximum temperature was approximately 1040 to 1060 °C, stand in marked contrast. The F-10 material shows a significantly higher ductile-to-brittle transition temperature of approximately 70 °C as well as a lower upper-shelf energy of approximately 120 J. These differences reflect the reduced ductility and impact resistance that was produced in this material by the high temperatures and relatively rapid cooling associated with the accident.

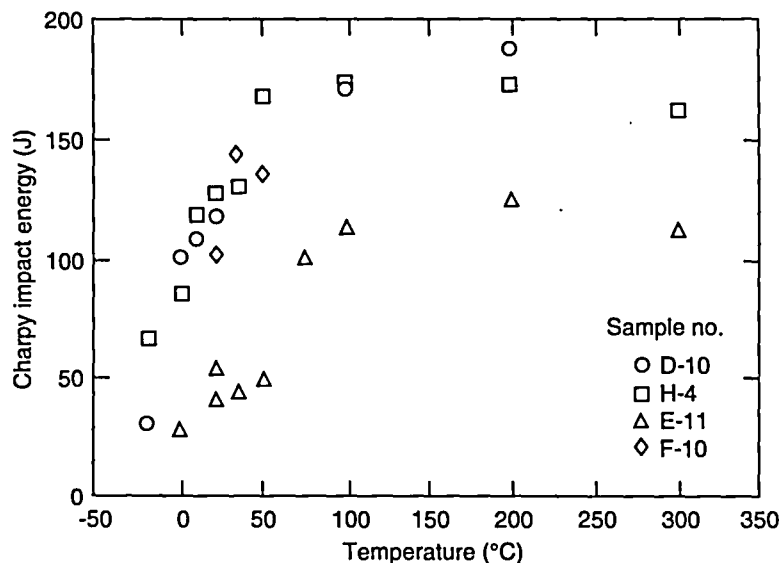


Fig. 10 Absorbed impact energy vs. test temperature.

SUMMARY AND CONCLUSIONS

The INEL, ANL, and the OECD partner laboratories have conducted microstructural characterization and mechanical property tests on material from 15 locations in the lower head of the pressure vessel of the TMI-2 nuclear reactor. The microstructural characterization was conducted by conventional optical metallography, hardness measurements, scanning electron microscopy of etched specimens and surface replicas, and analytical transmission electron microscopy of thin foils and carbon extraction replicas. The mechanical tests consisted of tensile tests at room temperature, tensile and creep tests at 600 to 1200 °C, and Charpy-impact tests at -20 to +300 °C. The specimens were taken from locations where the maximum temperature had not exceeded 727 °C during the accident and from locations where the maximum temperature had been as high as 1100 °C. The results of these investigations lead to the following conclusions:

1. An elliptical hot spot approximately 1 × 0.8 m on the inside surface of the lower pressure vessel head was heated to temperatures from approximately 800 to 1100 °C for approximately 30 minutes by relocated fuel debris.

2. The remainder of the lower head remained below 727 °C, but some areas may have been almost this temperature.

3. The temperature gradient through the thickness of the vessel wall was approximately 2 to 4 °C/mm.

4. The thermal excursion of the lower head was "quenched" (i.e., cooled at approximately 10 to 100 °C/min).

5. The results of tensile tests conducted on base-metal specimens for which the maximum temperature during the accident (T_{max}) did not exceed 727 °C agree well with literature data for A 533 B steel and show a dramatic drop in strength at temperatures above 600 °C.

6. Tensile specimens from samples for which T_{max} exceeded 727 °C showed significantly higher strengths at room temperature and 600 °C when compared with specimens for which the temperature did not exceed 727 °C.

7. Stress-rupture tests at 600 and 700 °C indicated no significant difference in behavior between base-metal specimens for which T_{max} was approximately 727 °C and those for which it was well below this value.

8. The stress-rupture data obtained from base-metal specimens were fit with both the Larson-Miller and Manson-Haferd time-temperature parameters.

9. Charpy V-notch impact tests conducted on lower-head base-metal material revealed a substantial difference between specimens from sample F-10, for which T_{max} was approximately 1040 to 1060 °C, and specimens from samples for which T_{max} was less than 727 °C. The F-10 material showed a significantly higher ductile-to-brittle transition temperature as well as a lower upper-shelf energy value.

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Margin-to-Failure Calculations for the TMI-2 Vessel^a

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Abstract: As part of the Three Mile Island Unit 2 (TMI-2) Vessel Investigation Project (VIP) sponsored by the Organization for Economic Cooperation and Development (OECD), margin-to-failure (MTF) calculations for mechanisms having the potential to threaten the integrity of the vessel lower head were performed to better understand events that occurred during the TMI-2 accident. Analyses considered four failure mechanisms: penetration tube rupture, penetration tube ejection, global vessel rupture, and localized vessel rupture. Calculational input was based on data from the TMI-2 VIP examinations of the vessel steel samples, penetration tube nozzles, and samples of the hard layer of debris found on the TMI-2 vessel lower head. Sensitivity studies were performed to investigate the uncertainties in key parameters for these analyses. Calculation results indicate that less margin existed for vessel failure mechanisms, rather than tube failure mechanisms, during the TMI-2 accident. In addition, calculations suggest that additional experimental data are needed to reduce uncertainties in models for predicting debris cooling and vessel failure.

On March 28, 1979, the Three Mile Island Nuclear Station Unit 2 (TMI-2) pressurized-water reactor underwent a prolonged, small-break loss-of-coolant accident that severely damaged the reactor core. The postulated end-state conditions of the TMI-2 reactor vessel and core are shown in Fig. 1. As illustrated in this figure, at least 45% of the core melted. Video examinations after the accident indicate that approximately 19 000 kg of molten material relocated from the core region to the water-filled, lower head of the reactor vessel. Examinations indicate that relocated debris severely ablated several instrument tube penetrations inside the lower head, although instrument tubes appeared to be protected at the

point where they were welded to the lower head. Instrument tubes outside the vessel and the vessel lower head, however, remained intact throughout the accident. Metallurgical examinations indicate that a localized region of the vessel, approximately 1 m by 0.8 m, reached temperatures between 1075 and 1100 °C during the accident; these examinations also indicate that vessel temperature away from the hot spot did not exceed 727 °C during the accident. However, these temperatures are well above the 538 °C maximum operating temperature limit considered in Case N-499 of the American Society of Mechanical Engineers (ASME) Boiler and Pressure Vessel Code.¹

As part of the TMI-2 Vessel Investigation Project (VIP), margin-to-failure (MTF) analyses were performed to increase understanding of the events that occurred during the TMI-2 accident. Calculations were performed considering four vessel lower-head failure mechanisms: penetration tube rupture, penetration tube ejection, global vessel rupture, and localized vessel rupture. Although experimental data have validated many aspects of severe accident analyses models, no integral experimental data are available to validate entire models. Hence the data available from the TMI-2 VIP, previous TMI-2 research programs, and plant instrumentation provide a unique opportunity to assess uncertainties in severe accident analyses models.

This article summarizes models used in the MTF analysis effort. Significant results from these calculations are also presented. A more complete description of the analyses and results can be found in Ref. 2.

APPROACH

Figure 2 depicts the four failure mechanisms considered in these analyses. The tube rupture failure mechanism (part a of Fig. 2) may result from a combination of high pressure and elevated ex-vessel tube temperatures as the result of contact with debris that has traveled through the tube to ex-vessel locations. Failure of a penetration tube weld (part b of Fig. 2) could result from

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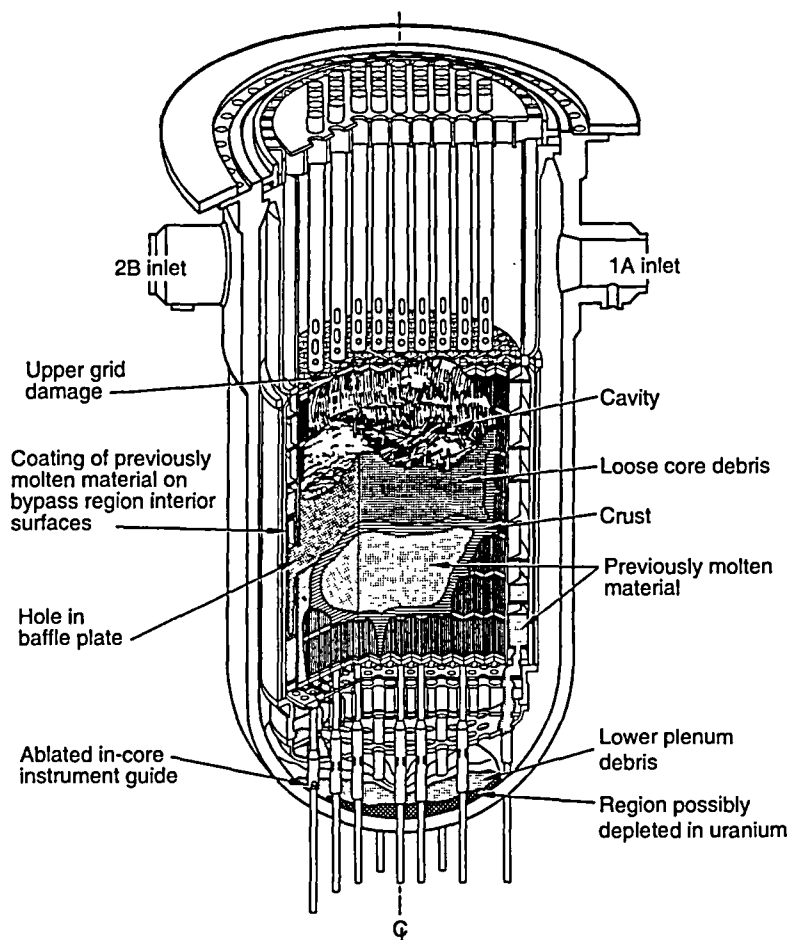


Fig. 1 Postulated TMI-2 end-state configuration.

debris melt attack and sustained heating from accumulated debris around the perimeter of a tube combined with reactor system pressure. Once the weld has failed, tube ejection is possible. Global vessel rupture (part c of Fig. 2) may be caused by elevated system pressure and/or the weight of debris on the lower head in conjunction with sustained heating from debris on the lower head. Localized vessel rupture (part d of Fig. 2) may be caused by thermal loads on the lower head as the result of nonuniform heat sources within the debris bed or a coherent jet of debris impinging directly onto the lower head in conjunction with mechanical loads caused by system pressure and debris weight.

As discussed previously, data from the TMI-2 VIP provide a unique opportunity to assess uncertainties in severe accident analysis tools. Little, if any, validation

has been performed on methods used to predict melt-water interaction, molten pool behavior, cooling in debris that solidifies after relocation, and structural creep failure in a severe accident. Thus this calculational effort is useful not only because it provides insights into what failure mechanisms were plausible during the TMI-2 event and identifies the failure mode with the smallest margin during the TMI-2 event but also because it indicates areas where additional data are needed for severe accident modeling.

Calculations relied on VIP examination data from the TMI-2 instrument nozzles, the hard layer of debris found on the head (the "companion debris samples"), and the TMI-2 reactor vessel steel (the "vessel boat samples"). Metallurgical examination data were used to characterize peak vessel temperatures, the duration of

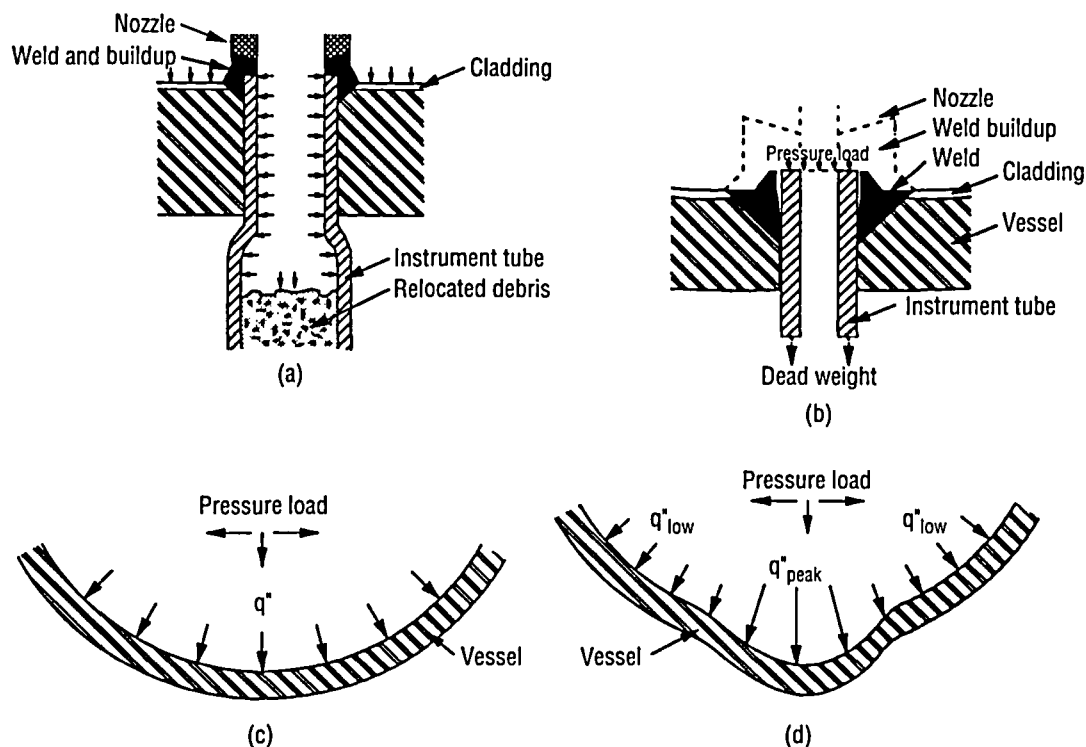


Fig. 2 Failure mechanisms considered in TMI-2 MTF analyses: (a) tube rupture, (b) weld failure-tube ejection, (c) global vessel failure, and (d) localized vessel failure.

peak temperatures, vessel cooling rates, and the end state of instrument nozzle weld material.^{3,4} Data from examinations of companion debris samples were used to characterize such debris properties as decay heat and material composition.⁵ Nozzle examination data were used to characterize the composition of melt attached to nozzles, elevations for nozzle ablation heights in the vessel, and melt penetration distances within nozzles.⁶ Uncertainties for each data source are discussed in Ref. 2. Calculations included sensitivity studies to consider the range of input associated with uncertainties in data.

The potential for each failure mechanism to occur was evaluated on the basis of both ultimate strength and creep damage. Ultimate strength MTF was defined by

$$\text{MTF} = (1 - \text{effective stress/ultimate strength}) 100\%$$

The TMI-2 Structural Mechanics Peer Review Group defined by consensus a separate stress-based MTF for creep failure.⁷ The procedure includes converting multidimensional stress history to an effective stress

(equivalent uniaxial stress) history and predicting time to failure for the converted stress and temperature histories using a time-damage model. When results from the initial scoping calculations suggested that a stress-based failure criterion may be too conservative for the prediction of failure, calculations were performed in which creep failure was defined as the point at which strain instability occurred (strain rate approaches infinity).⁸

The MTF calculations investigated an inconsistency between companion debris sample data, which suggest slow debris cooling, and vessel steel sample examination data, which imply relatively fast vessel cooling rates. When results primarily obtained from input based on companion debris sample data indicated that vessel failure would occur, irrespective of which failure criterion was selected, it was postulated that additional cooling (not currently modeled in severe accident analysis codes) occurred. A thermal analysis based on plant thermal hydraulic parameters measured or inferred from data measured during the accident [coolant temperature, reactor coolant system (RCS) pressure,

coolant flow rates entering and exiting the vessel, etc.] confirmed that more cooling than currently considered in severe accident analysis codes occurred during the period between debris relocation and vessel repressurization. Hence calculations were performed to quantify the magnitude of this cooling and possible debris configurations that could explain how this cooling could have occurred.

HIGHLIGHTS FROM MTF ANALYSIS RESULTS

Results from scoping calculations, which evaluate each of the failure mechanisms identified are reviewed in the following text. Results from thermal analyses, required as input for structural response calculations, are also discussed. Finally, results from calculations performed to assess sensitivity to debris cooling rates and different failure criteria are presented.

Melt Penetration—Ex-Vessel Tube Rupture

For ex-vessel tube rupture to occur, melt must travel through an ablated instrument tube to a distance that is below the vessel outer surface in part a of Fig. 2. Several models have been developed to predict the penetration distance of molten debris through vessel instrumentation nozzles. Although previous research was insufficient to select a model for predicting melt flow through light-water-reactor instrument tubes, melt penetration distances have been experimentally determined to be bounded by distances predicted by the bulk-freezing model and the conduction heat transfer model. The bulk-freezing model, first advanced by Ostensen and Jackson,^{10,11} assumes that turbulent heat transfer governs melt solidification and penetration behavior. The conduction heat transfer model, first advanced by Epstein,^{12,13} assumes that (as its name implies) conduction heat transfer governs melt solidification and penetration behavior.

Data from some TMI-2 instrument nozzles provide measurable distances for melt that traveled through in-vessel instrument structures during the TMI-2 accident. Longer nozzles containing melt with measurable penetration distances were used to select an appropriate model for estimating penetration distances; this model was then used to determine if melt could travel below the vessel lower head through shorter nozzles (see Fig. 3). Melt penetration distances predicted with a bulk-freezing model,⁹ modified to consider heat loss from the melt to

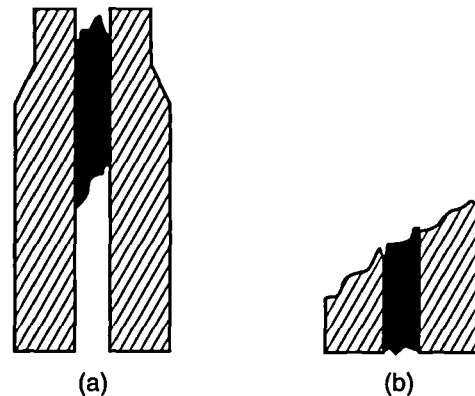


Fig. 3 Various configurations of melt observed in TMI-2 instrument nozzles: (a) nozzle stub containing melt with measurable penetration distance and (b) nozzle stub containing melt with unknown penetration distance.

the tube and the coolant, were found to be consistent with distances measured in TMI-2 instrument nozzles. Distances predicted with a conduction model,^{12,13} on the other hand, were found to be much longer (typically, several orders of magnitude longer) than melt penetration distances measured in TMI-2 instrument nozzles. Hence the modified bulk-freezing model was determined to be more appropriate for estimating the melt penetration distances observed in the TMI-2 nozzles.

Melt penetration distances predicted with the modified bulk-freezing model indicate that fuel containing molten debris would not travel through instrument tubes to locations below the lower head. Calculations bounded possible melt compositions, temperatures, and melt flow areas to maximize penetration distances. Furthermore, the nozzle stub height was assumed as 1.3 cm, which was the smallest ablated nozzle height observed in TMI-2 defueling efforts.¹⁴ Although calculations indicate that it is possible for molten debris with highly metallic compositions to flow to ex-vessel tube locations, previous review of TMI-2 instrumentation data¹⁵ suggests that metallic material quenched when it relocated to the lower head during the TMI-2 accident. Hence ex-vessel tube temperatures are not predicted to be higher than the RCS temperatures. Therefore ex-vessel tube rupture calculations were performed assuming that the tube temperatures were consistent with the vessel coolant temperatures.

A simple model comparing the pressure force on the tube and the tube's ultimate strength was used to evaluate ex-vessel tube rupture. As discussed previously, tube

temperatures for these analyses were assumed to equal the vessel coolant temperature. An upper bound on the coolant temperature was taken to be a representative saturation temperature (327 °C) corresponding to system pressures during the first 12 hours after the major relocation of fuel occurred; a lower-bound temperature was based on the minimum temperature (127 °C) measured in the cold legs during the transient. Although ultimate strength data for Inconel are limited,⁹ data shown in Fig. 4 indicate that the ultimate strength for the TMI-2 Inconel instrument tubes is above 700 MPa for the temperatures of interest (127 to 327 °C). Because such temperatures were expected to result in very high MTFs, a conservatively high constant upper system pressure of 15 MPa was also applied in the tube rupture calculations. Thus calculations indicate that ultimate-strength MTF for tube temperatures of 127 and 327 °C are both above 95%. Times to creep rupture at these temperatures are estimated to be on the order of 10^{15} and 10^{29} hours. Hence ex-vessel tube rupture can effectively be eliminated as a potential failure mechanism for this accident.

Jet Impingement-Vessel Thermal Response

Calculations were performed to investigate melt relocation and the subsequent thermal loading to the vessel during the TMI-2 accident. Results from these calculations provide input to subsequent weld failure, global vessel failure, and localized failure analyses. Analytical models were applied to simulate the debris-vessel interaction to investigate the thermal response of the vessel during and after debris relocation. These

models include phenomena such as breakup of melt relocating into and through the lower plenum water, growth of the debris pool and its associated top and bottom crusts, heat fluxes delivered to the vessel inner surface, and the resulting vessel temperature distribution. Because considerable uncertainty is associated with many input parameters for these models, studies were performed considering lower-bound, upper-bound, and best-estimate (or nominal) values for input parameters related to debris decay heat, debris relocation mass, and heat transfer from the debris and the vessel. Many of the input parameters for the thermal analysis were based on companion debris sample examination data (debris composition, decay heat levels, and "slow cooling" evidence).⁵ Results from the thermal analyses were compared with results from vessel steel sample examinations (peak hot spot and global vessel temperatures, duration of peak hot spot temperatures, and cooling rate of vessel in the hot spot location).⁴

The potential for melt to disperse and quench as it passes through the flow distributor plate and into the water-filled lower plenum was analyzed with the TEXAS fuel-coolant interaction (FCI) model.¹⁶ TEXAS predicts the behavior of molten fuel interacting with water during the mixing and propagation phases of a molten FCI. As with many phenomena considered in severe accident analysis codes, considerable uncertainty may exist in TEXAS results because of limited data for validating FCI codes; however, various TEXAS sensitivity studies were used to address the impact of code modeling uncertainties. Sensitivity studies were also used to assess the impact of input data uncertainties. Posttest examination data and plant instrumentation data indicate that the major relocation of melt occurred within a 2-minute time period during the accident (224 to 226 minutes after reactor scram). Calculations considered total mass flow rates ranging from 300 to 1000 kg/s to address uncertainties in mass flow rates, although the duration of the jet pour was reduced to keep the total mass that relocated constant. Because melt may have drained from more than one of the holes in the elliptical flow distributor plate, analyses considered one and three jet cases. For all the cases, the system pressure was 10 MPa, which was the reactor vessel pressure during the time period when most debris relocation is postulated to have occurred. Jets were assumed to pour through coolant at saturated and subcooled conditions (the amount of subcooling was bounded by the temperatures measured in the RCS cold leg). The temperature of the melt at injection was assumed as 2630 °C, the liquidus temperature for the

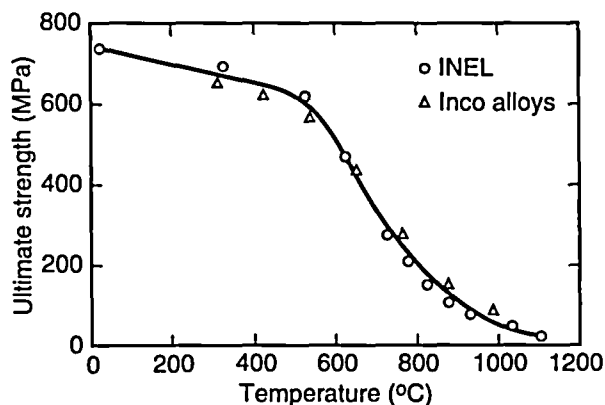


Fig. 4 Inconel-600 ultimate strength as a function of temperature.

composition of the melt identified in the companion sample examinations.⁵

Simulation results from the TEXAS fuel-coolant interaction model indicate that insignificant amounts of melt dispersal or "breakup" occur as melt relocates to the lower head. Maximum breakup was obtained for cases in which three jets were assumed to be present; however, even in these cases, less than 1% of the relocating material is estimated to break away from the jet and quench. When the breakup was predicted to be insignificant, the analyses of fuel relocation continued under the assumption that molten debris reached the lower plenum in a substantially liquid state, ultimately impinging on the vessel. Because vessel thermal response calculations indicate that the molten material that relocated to form the "hard layer" could, by itself, impose a thermal load resulting in temperatures that exceeded peak values estimated from metallurgical examinations and because there is uncertainty about when the additional rubble on top of the hard layer relocated, no further assessment of the impact of the rubble on vessel thermal response was performed.

A model was developed to estimate heat transfer to the vessel from jet impingement and natural convection in the molten pool.¹⁷ The model assumes that one jet impinges at the center of the lower head and a crust forms on the lower head as soon as the melt contacts it. Heat is then transferred through the crust to the vessel at locations where the melt is in contact with the vessel. When the molten jet stops draining and surface agitation is reduced, a crust may form on pool upper and lower surfaces. An energy balance is used in the model to determine the size of the crusts and melt pool. A detailed description of this model may be found in Refs. 2 and 17.

Sensitivity calculations considered the vessel thermal response using various combinations of upper-bound, lower-bound, and best-estimate values for input parameters, such as debris-to-coolant heat transfer, debris decay heat, debris-to-vessel thermal contact, and heat removal from the vessel. Results from several sensitivity studies revealed a consistent vessel thermal response; namely, the thermal response can be divided into three time periods: (a) an initial localized temperature excursion over the time and location of jet impingement (typically lasts for about 1 minute); (b) a transient vessel heatup (typically lasts for about 1 hour); and (c) a quasi-steady vessel temperature distribution (typically lasts for several hours). Best-estimate input values used for a case with nominal input parameters resulted in global peak temperatures of more than 900 °C, which is inconsistent

with metallurgical examination data. Only a case with lower-bound input assumptions results in temperature predictions that are, considering the uncertainty range associated with these predictions, consistent with metallurgical examination data; namely, that global vessel temperatures remain below values at which the material undergoes a transition from ferritic to austenitic steel (727 °C).

Results from jet impingement and vessel thermal response calculations indicate that the magnitude and duration of the hot spot temperatures estimated in TMI-2 vessel examinations could not have been caused by an impinging jet because peak temperatures during melt relocation are typically not predicted to be sustained for more than a few minutes (instead of the 30-minute duration indicated by vessel examinations). Hence it is postulated that hot spot temperatures occurred later in the scenario because of a sustained heat load from debris resting on the lower head. The limited area estimated to have experienced hot spot temperatures suggests that this region was subjected to a localized heat source, such as might occur with a nonhomogeneous debris bed or a localized region with better contact between the debris and the vessel.

Weld Failure—Tube Ejection

Before the performance of a tube ejection analysis, it must be established that the nozzle-to-vessel weld failed. Because it is not known if the hot spot temperatures occurred when the RCS was at high pressure, weld failure calculations were performed with the use of a simple model based on force equilibrium (see part b of Fig. 2) in which it was conservatively assumed that peak temperatures and pressures occurred simultaneously. Metallurgical evidence from TMI-2 examinations indicates that the Inconel penetration welds did not melt.³ Hence peak temperatures inferred from metallurgical examinations of vessel specimens from the hot spot region (less than 1100 °C)⁴ were assumed in these calculations. The maximum value of RCS pressure measured after melt relocation, 15 MPa, was assumed for system pressure in these calculations. Shear stress at the weld-tube interface was calculated, converted to effective stress, and used in the MTF calculations.

Results indicate that, even for these conservative assumptions, there was considerable margin in the weld's integrity. Nominal case calculations based on nominal input indicate that the ultimate-strength MTF is 60%. Lower- and upper-limit estimates of the ultimate-strength MTF were 54 and 65%, respectively. If the peak hot spot

temperature and a 15 MPa system pressure were both maintained constant, the time to creep failure is estimated as 7.2 hours with upper and lower estimates of 4.2 and 16.9 hours, respectively. The large ultimate-strength MTF and the long estimated time to creep failure are conservative for several reasons. One reason is that the analysis assumed a constant pressure of 15 MPa, whereas the peak temperatures may have occurred at a lower pressure. Furthermore, calculations assumed that the peak temperature remained constant when, in fact, the peak temperature was estimated to last for only 0.5 hour.⁴ In addition, the load-bearing weld area was minimized by ignoring the weld buildup material above the stainless steel cladding and using a minimum weld depth into the vessel. Finally, the load was assumed to be carried solely by the weld, and none of the load was distributed to the tube support located beyond the tube bend outside the vessel. Because penetration weld integrity during the TMI-2 accident was predicted in this very conservative analysis, penetration tube ejection was ruled out as a possible failure mode.

Global Vessel Failure

Two models were used to assess vessel structural response. The first is a simpler, one-dimensional (1-D) model imposing global force equilibrium in spherical geometry, and the second is a more sophisticated, two-dimensional (2-D) model. The 1-D model was applied to provide an initial, rough estimate of failure times. Although this model was quicker and easier to apply, uncertainties associated with 2-D and stress redistribution effects required the more detailed 2-D model.

In the 1-D model, average radial and hoop components of stress are used to define effective stress, as formulated by Huddleston.¹⁸ Creep damage is tracked as a function of stress and temperature at 20 equally spaced layers through the thickness of the vessel. Damage within a particular time interval and at a given location is defined on the basis of the effective stress and temperature through the use of a Larson-Miller Parameter (LMP).¹⁹ The LMP is used to obtain a rupture time, t_r , under the stress and temperature conditions. Incremental damage, d , within a time increment, Δt , is defined as $d = \Delta t/t_r$. As the thermal transient proceeds, the accumulated damage is summed from the incremental damage. When the accumulated damage exceeds unity in a particular layer, that layer of the vessel is removed from the calculated load carrying capacity of the vessel. As discussed previously, MTF is defined as the difference between unity and the ratio of load to load-carrying-

capacity. As more layers experience 100% damage, the load-carrying-capacity continues to diminish. Vessel failure is defined as the time when MTF becomes zero.

The 2-D model is an axisymmetric variation of a finite deformation shell theory described in Ref. 20, and the details of the adopted form of the method are described in Ref. 9. The shell theory allows for thermal, plastic, and creep as well as elastic strains but is not as general as an axisymmetric continuum model in that the radial stress is neglected, normal strains are assumed to vary linearly through thickness, and shear strains are assumed to vary parabolically through thickness. The assumed through-thickness behavior permits enforcement of vertical and horizontal force equilibrium and moment equilibrium through integrated force and moment resultants.

Implementation of the stress-based failure criterion in the 2-D model differs slightly from that used in the 1-D model. In the 2-D model, the vessel is divided in the radial direction into ligaments; ligament behavior is allowed to vary continuously in the meridional direction. Stress can vary in both the radial and meridional directions, whereas the simpler 1-D model uses average radial and hoop stresses. Incremental and accumulated damage are evaluated the same way for both models, but when a ligament becomes fully damaged in the 2-D model, it is "clipped," which means the stress state is set to zero and equilibrium necessitates redistribution of stresses to the remaining, intact ligaments. In this stress-based criterion, failure occurs when all the ligaments become fully damaged through thickness at any one location.

Figure 5 compares results from the 1-D model and the 2-D model for the vessel subjected to lower-bound heat fluxes. Parts a and b of Fig. 5 illustrate output from the 1-D model. These parts illustrate the phasing of vessel wall temperature, system pressure, the calculated MTF history, and the timing of vessel layer failure during the accident. As shown in part a of Fig. 5, MTF starts at 80%, reduces to approximately 45% at the 2-hour mark, and quickly drops to 0.0% afterward. Layers of the vessel start to fail after 2.0 hours, and all the layers have failed at 2.3 hours (part b of Fig. 5). Thus the 1-D model predicts failure in slightly less than 2.3 hours.

Part c of Fig. 5 illustrates accumulated damage as calculated from the 2-D model. Damage is defined in the 2-D model as the *average* of the damage evaluated at all integration points along the shell's meridian, so accumulated damage never exceeds unity. This definition is more appropriate for the 2-D model because the number of nodes is variable. As discussed previously, failure is defined in the 2-D model as the time when all

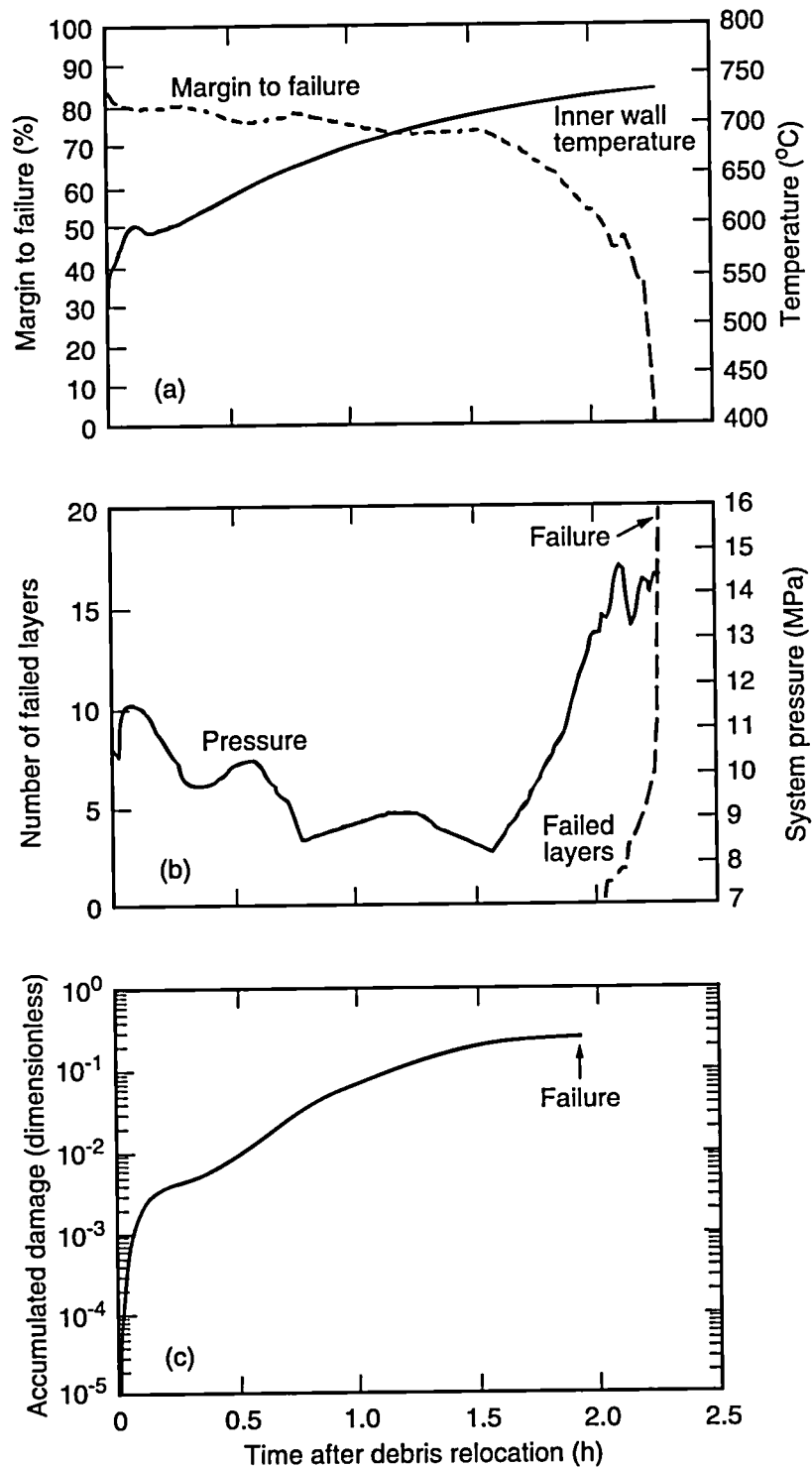


Fig. 5 Comparison of one- and two-dimensional model results for lower-bound case: (a) MTF (1-D model), (b) number of failed layers (1-D model), and (c) accumulated damage (2-D model).

the ligaments become fully damaged at any one location along the shell's meridian. The 2-D model predicts failure at approximately 1.9 hours.

Temperature distributions based on input from companion debris sample examination data (i.e., slow cooling of debris) resulted in calculations from both models predicting vessel failure. Although the inclusion of stress redistribution and 2-D effects in the 2-D model decreased failure predictions by approximately 0.4 hour, both models predict vessel failure at approximately 2 hours. Obviously, this did not occur. Hence it appears that global vessel temperatures must have decreased within 2 hours after core relocation. Hence it is postulated that additional debris cooling, not modeled in these initial calculations based on companion debris sample examination data, occurred within the first 2 hours after melt relocation.

Localized Vessel Failure

The potential for the vessel to experience a localized failure was also evaluated by application of an elevated heat flux over a localized region, which resulted in temperatures and temperature gradients consistent with metallurgical observations of the TMI-2 vessel steel samples.⁴ The 2-D structural model used in the global vessel failure analyses was applied to calculate thermal, plastic, and creep strains when the vessel is subjected to a localized heat source.

To understand the relative roles of the hot spot temperature distribution and the global background

temperature distribution outside the hot spot, two cases were considered: (a) hot spot temperatures imposed on top of global temperatures estimated for the lower-bound case (see discussion in Jet Impingement/Vessel Thermal Response) and (b) hot spot temperatures imposed on a vessel with cool background temperatures (327 °C inner surface, 277 °C outer surface). These two temperature distributions bounded possible background distributions inferred from vessel steel sample examinations. In these calculations, failure was predicted to occur in 1.5 hours for Case (a), and the vessel was predicted to survive for Case (b).

The effect of a hot spot was evaluated for a shell with a cool background [Case (b)] to confirm that the metallurgically estimated hot spot temperatures alone would not result in a localized vessel failure. Because metallographic examinations of vessel specimens outside the hot spot indicated only that the vessel did not reach the ferritic-to-austenitic transition temperature (approximately 727 °C), global vessel temperatures could have been considerably lower than this transition temperature. (Note that peak values predicted in the lower-bound temperature distribution were approximately equal to the transition temperature.) The initial temperature distribution from the lower-bound case was used to bound possible temperatures in this cooler case; that is, a linear temperature distribution through the thickness with a 327 °C inner surface and a 277 °C outer surface.

The structural response results for Case (b) are in Fig. 6, which shows damage rate vs. time. Note that for

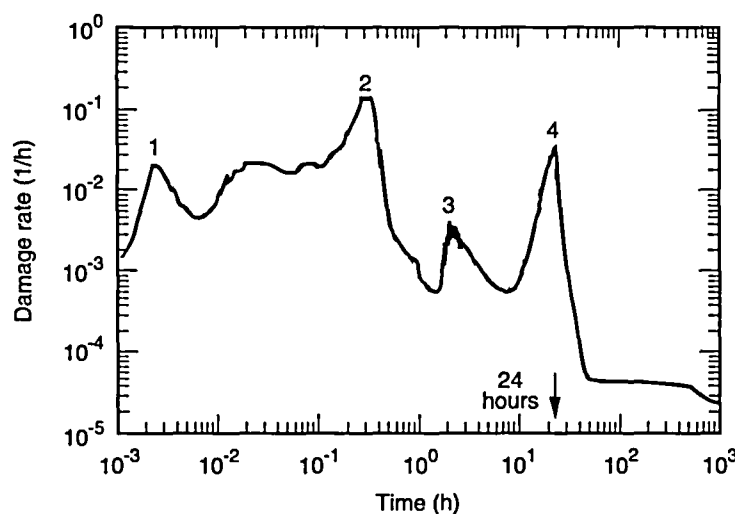


Fig. 6 Damage rate vs. time for localized failure analysis of hot spot temperatures on a cool background.

the 2-D structural model damage is defined in the 2-D model as the *average* of the damage evaluated at all integration points along the shell's meridian. Four important peaks, labeled 1 through 4 in Fig. 6, are in the damage rate.

The first peak (point 1 in Fig. 6), which occurs between 3 and 30 seconds, was associated with the thermal shock (i.e., the nodes on the inner surface experienced a relatively severe damage rate as they reached temperatures in excess of 1027 °C, yielding in compression as they expanded against the cooler shell). This severe damage rate was diminished as the temperature front moved into the interior wall of the vessel.

The second peak (point 2 in Fig. 6) occurs at just over 1000 seconds into the transient and represents the largest rate (0.1 h⁻¹) at any time during the transient. This state occurred when the temperature front had elevated the outer surface temperatures to levels of 527 to 577 °C. The outer surface material was supporting a large tensile stress (~250 MPa) and at this temperature experienced both a high damage rate and creep rate. The damage rate dissipated when the temperature front completely penetrated the shell and thus pushed the outer surface temperature above 727 °C, which reduced the temperature gradient and associated stresses.

At 1.6 hours into the TMI-2 transient, the system was repressurized, and the damage rate experiences a third peak (point 3 in Fig. 6), although of substantially lesser size than the transient heat-up peak. The fluctuations in the repressurization peak mirror the fluctuations in the TMI-2 pressure history associated with relief valve opening and reseating. Although the transient pressure fluctuations continued until 260 minutes after relocation, these calculations assumed a constant pressure for time periods greater than 180 minutes after relocation and thus caused the fluctuations to disappear from the damage rate plot after this time. Repressurization to 14.5 MPa at 2.1 hours also corresponds to the attainment of maximum temperatures in the shell, so the damage rate decreased shortly after repressurization as the shell cooled.

The final damage rate peak (point 4 in Fig. 6) occurs approximately 24 hours after the major melt relocation occurred and is associated with cooldown. During the heat-up and high-temperature periods, material near the inner surface of the vessel at its base experienced compressive stress and underwent negative creep strain under compressive load. As the vessel cooled, this material then contracted and experienced tension. As the material temperature dropped during the cooldown period, tensile stresses on the bottom inner surface exceeded +100 MPa and thus caused rapid damage

accumulation and the damage rate peak at 24 hours, which is shown in Fig. 3.

The Structural Mechanics Peer Review Group⁷ defined MTF for creep to be the difference between time to failure and the time at which pressure and temperature states are fixed at points of maximum damage rate. Hence the MTF for this case was evaluated by assuming constant temperature and pressure conditions for each of the peaks in Fig. 3 and predicting time to failure, as discussed in the Approach. The initial peak associated with the thermal shock (during melt relocation) was not relevant to the MTF analysis because only the material on the inner surface experienced elevated temperatures during the first 30 seconds of the transient. Hence MTF for this case is the minimum failure time estimated in MTF calculations for peaks 2, 3, and 4 in the damage rate curve. The minimum MTF was obtained by fixing the pressure and temperature conditions corresponding to peak 3. The MTF for this is estimated at 8 hours.

The cases examined in this localized vessel failure analysis indicate that background temperatures play a pivotal role in determining whether the vessel is predicted to survive. The vessel is predicted to fail when hot spot temperatures are superimposed on a global temperature distribution obtained with heat fluxes corresponding to lower-bound input assumptions; however, the vessel can survive local hot spots in the temperature range and of the duration inferred from TMI-2 metallurgical examinations, but the balance of the shell must remain cool.

Sensitivity to Debris Cooling and Failure Criterion

As noted previously, thermal analyses were performed on the basis of debris properties (decay heat levels, "slow cooling" evidence) from the companion debris sample examinations; however, thermal and structural calculational results combined with metallurgical examination results suggest the hypothesis that some form of cooling occurred that was not evident in the TMI-2 companion debris samples. In addition, analysis results suggest that the stress-based failure criterion that is used to predict failure may be too conservative. Analyses performed to investigate the effects of debris cooling and failure criterion on calculational results are discussed in the following text.

Changes in Debris Internal Energy After Relocation

Initial scoping calculation results suggest that some form of debris cooling occurred within the vessel after a

major relocation occurred (approximately 224 minutes) and before the vessel was repressurized (approximately 320 minutes). Through the application of some simplifying assumptions related to heat transfer within the vessel, equations for volume, mass, and energy conservation were used to obtain an order-of-magnitude estimate of the change in debris internal energy after debris relocation. Sources of coolant entering the vessel during the time period of interest include normal RCS makeup and high-pressure injection from the emergency core cooling system. Sources of coolant exiting the vessel during this time period include normal RCS letdown and coolant flowing out the open power-operated relief valve (PORV). These coolant flow rates and associated uncertainties were quantified with results from previous analyses of plant data.²¹⁻²⁴ The amount of decay heat input to the system was quantified with information in Ref. 25 to account for the reduction caused by volatile fission-product release.

Calculation results indicate that the debris internal energy decreased between relocation and vessel repressurization. Calculations considered upper and lower bounds for all the input parameters, such as coolant flow rates entering and exiting the vessel and debris decay heat levels. Hence results from these scoping calculations should be viewed as order-of-magnitude estimates; however, results indicate that a negative change in debris internal energy occurred for the time period of interest in all the cases considered and support the hypothesis that debris cooling occurred that was not evident in the TMI-2 companion debris samples. Although considerable uncertainty is associated with these results, scoping calculations suggest that the estimated decrease in debris internal energy is sufficient for all the debris that relocated to the lower head to solidify and experience a decrease in temperature ranging from 420 to 2250 °C.

Slow and Rapid Cooling Analysis

Although there are insufficient data from the companion debris samples to determine the exact mechanisms that caused the rapid cooling of the debris within the first 2 hours after relocation, two possible forms of cooling were investigated. The first form of cooling considered was a slow cooling mode in which channels or cracks in the debris allowed for infusion of water that cooled the debris near the channels but left interior portions hot. This slow cooling was investigated by analyzing cases with a hot spot temperature distribution superimposed on 25, 33, and 50% of the background heat fluxes obtained

using nominal case input values. Results, summarized in part a of Fig. 7, indicate that the vessel would fail at 2.6 hours for a hot spot on a background equal to 50% of nominal case heat flux, but the vessel would survive on a background of 25% of nominal case heat flux. For the 33% of nominal case, results in part a of Fig. 7 indicate that the damage rate begins to rise during the repressurization period, which implies that failure is imminent. Depressurization 4 hours into the transient enables the vessel to survive a couple of hours longer, but ultimately the vessel is predicted to fail at 6.5 hours after melt relocation. These results indicate that, under slow cooling conditions and with a stress-based failure criterion, the vessel can survive a hot spot in the presence of background heat fluxes between 25 and 33% of nominal values.

The second form of cooling considered was a rapid cooling mode in which gaps or channels between the lower debris crust and the vessel allowed relatively high flow rates of coolant between the debris and the vessel. (These high flow rates rapidly cooled the vessel and outer portions of the debris but left interior portions of the debris relatively hot.) Analyses were performed to investigate the cooling needed to obtain vessel cooling rates consistent with the values observed in metallurgical examinations of specimens in the hot spot region, namely, that vessel specimens from the hot spot region underwent cooling rates between 10 and 100 °C/min in the ferritic-to-austenitic transition temperature region (727 to 827 °C) at approximately 30 minutes after the hot spot reached 1047 °C. Rapid cooling calculations were performed for cases of hot spot temperatures on 33 and 50% of nominal background heat fluxes. The heat sinks required to obtain these cooling rates were 25 and 125 kW/m², respectively. Under rapid cooling conditions, it is concluded that the structure must be close to failure before initiation of cooling for the vessel to subsequently fail. For these conditions, additional damage or strain accumulated during the cooldown period is minimal. The difference between cooling rates is exhibited in the timing and magnitude of damage peaks associated with cooldown. The faster cooling rate produces higher tensile stresses earlier in the transient, which results in an earlier and larger damage rate peak. Unlike the case illustrated in Fig. 6, however, the structure moves through this peak quickly, with little additional accumulated damage, and the damage rate then falls rapidly to a benign level. Simulations were also run for a hot spot on 75% of the nominal heat flux, but these simulations predict vessel failure in a little over 2 hours. Hence the vessel can survive a hot

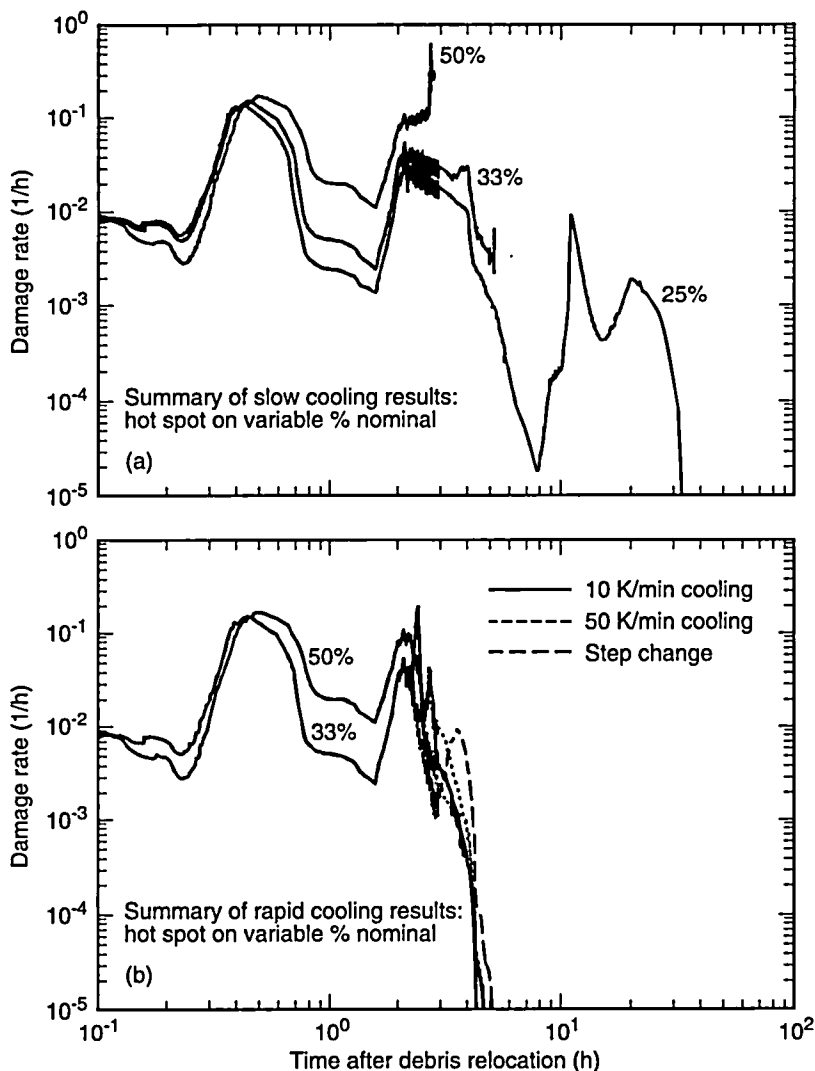


Fig. 7 Summary of slow and rapid cooling results obtained with a stress-based failure criterion: (a) slow cooling results and (b) rapid cooling results.

spot in the presence of background heat fluxes between 50 and 75% of the nominal case heat fluxes during the 30-minute time interval that hot spot temperatures are sustained and before the initiation of rapid cooling.

In summary, analyses indicate that both slow and rapid cooling occurred in some debris locations during the first 2 hours after melt relocation. If only a slow cooling mechanism were present, the vessel temperatures would not experience the rapid cooling rates observed in the metallurgical examinations. Furthermore, the vessel will not survive hot spot temperatures on the nominal case heat fluxes long enough to permit material to exist at

elevated (>1050 °C) temperatures for the 30-minute time period estimated in metallurgical examinations. Thus analyses indicate that both slow and rapid cooling mechanisms must be considered to obtain results consistent with TMI-2 VIP examinations.

Configurations to Obtain Required Cooling Rates

Although there are insufficient data to quantitatively determine the exact cooling mechanisms required to obtain a vessel response consistent with metallurgical data, scoping calculations were performed to investigate

the potential for channels and gaps within the debris to cause this cooling (the presence of this cooling would allow consistency of the companion debris sample data, the vessel steel sample data, and the thermal and structural response analyses). Estimating the number and size of debris channels and the size of debris-to-vessel gaps requires many assumptions related to debris properties and heat transfer parameters. This large uncertainty in input parameters was treated by estimating upper and lower bounds for each parameter and obtaining results by propagating upper- and lower-bound estimates. Lower-bound geometric parameters for channels within the debris and between the debris and the vessel were selected to minimize heat transfer capabilities. As discussed previously, results indicate that both rapid and slow cooling mechanisms were needed to be consistent with metallurgical examination data. Therefore it is assumed that the simultaneous presence of cracks and gaps within the debris provides multiple pathways for steam release (e.g., water may travel down along the gap and boil up through cracks). To maximize the number of cooling cracks and the gap size required to cool the debris, the heat transfer from the debris to the coolant was minimized by assuming that the coolant traveling through these cracks and gaps remained in a liquid state and neglecting any enhanced heat removal associated with subcooled or saturated boiling of the coolant.

Results indicate that a relatively insignificant volume of channels within the TMI-2 debris bed (<1% of the debris volume) could have removed a sufficient amount of heat to preclude vessel failure. Calculations also indicate that coolant traveling through a relatively small gap (a value of 1 mm was assumed) between the debris and the vessel could cause the vessel cooling rates estimated by metallurgical examination data. Although companion debris sample examinations did not substantiate the hypothesis that portions of the debris cooled within the first 2 hours, the mass of the companion debris samples was small compared with the mass that relocated (<7 kg of the 19 000 kg that relocated were examined).

Sensitivity of Results to Failure Criterion

Vessel deformation and damage distributions obtained in the initial scoping calculations indicate that failure strains are quite modest (<10%). For these reasons, the Structural Mechanics Peer Review Group suggested that another set of structural simulations be performed with a failure criterion based upon mechanical instability.⁸ Calculations were performed to investigate the influence of failure criterion on the amount of slow cooling needed

to preclude vessel failure and the amount of rapid cooling needed to obtain cooling rates consistent with the cooling indicated by metallurgical examinations. The characteristic deformations used to define instability are the maximum hoop strain, u/r_0 , located underneath the hot spot; the maximum vertical deflection, w , also located under the hot spot; and the maximum rotation of the shell meridian from its undeformed state, β , located somewhere in the cusped region of the undeformed shell.

In the slow cooling calculations, simulations were performed involving the hot spot on background heat flux distributions corresponding to 100, 75, 62.5, and 50% of the nominal case. Results for the 62.5 and 50% nominal cases are shown in part a of Fig. 8. For the 50% nominal case, the bulk of the vessel remains sufficiently stiff to restrain the hot spot region; consequently, tensile stresses in the hot spot region 4 hours after relocation are quite modest. When the system depressurizes at 4 hours, the vessel unloads elastically, and most of the vessel under the hot spot subsequently experiences compression. Under these conditions, the vessel creeps down in the hot spot region and u/r_0 decreases. Maximum values of w and β remain nearly constant.

Deflections for the case with 62.5% nominal are substantially greater than those for the case with 50%. When the vessel is less restrained, more tension exists, and no discernible decrease in hoop strain occurs when the pressure decreases. Once depressurization stops at 5.25 hours, the deformations again begin to increase. The increasing deflections near 6 hours for the 62.5% case suggest, however, that it is unlikely the vessel would survive upon complete repressurization to 16 MPa at 11 hours. It is concluded that, under slow cooling conditions and a deformation-based criterion, the vessel can survive a hot spot on a background heat flux between 50 and 62.5% of the nominal level.

In the rapid cooling calculations, simulations were performed for hot spots on background heat fluxes equal to 62.5, 75, and 80% of the nominal level. Results in part b of Fig. 8 indicate that the vessel easily survives rapid cooling from 62.5% of nominal, and all deformations asymptotically settle to benign values. When rapid cooling is initiated from hot spots on 75% of nominal, however, the vessel has already experienced substantial deformation before initiating cooling. The inspection of curves in part b of Fig. 8 indicates that during the cooling period the rotation β actually decreases but then begins to climb again once cooling is completed. The depressurization period between 4 and 11 hours greatly slows the rate of vessel deformation, but repressurization to 15 MPa at

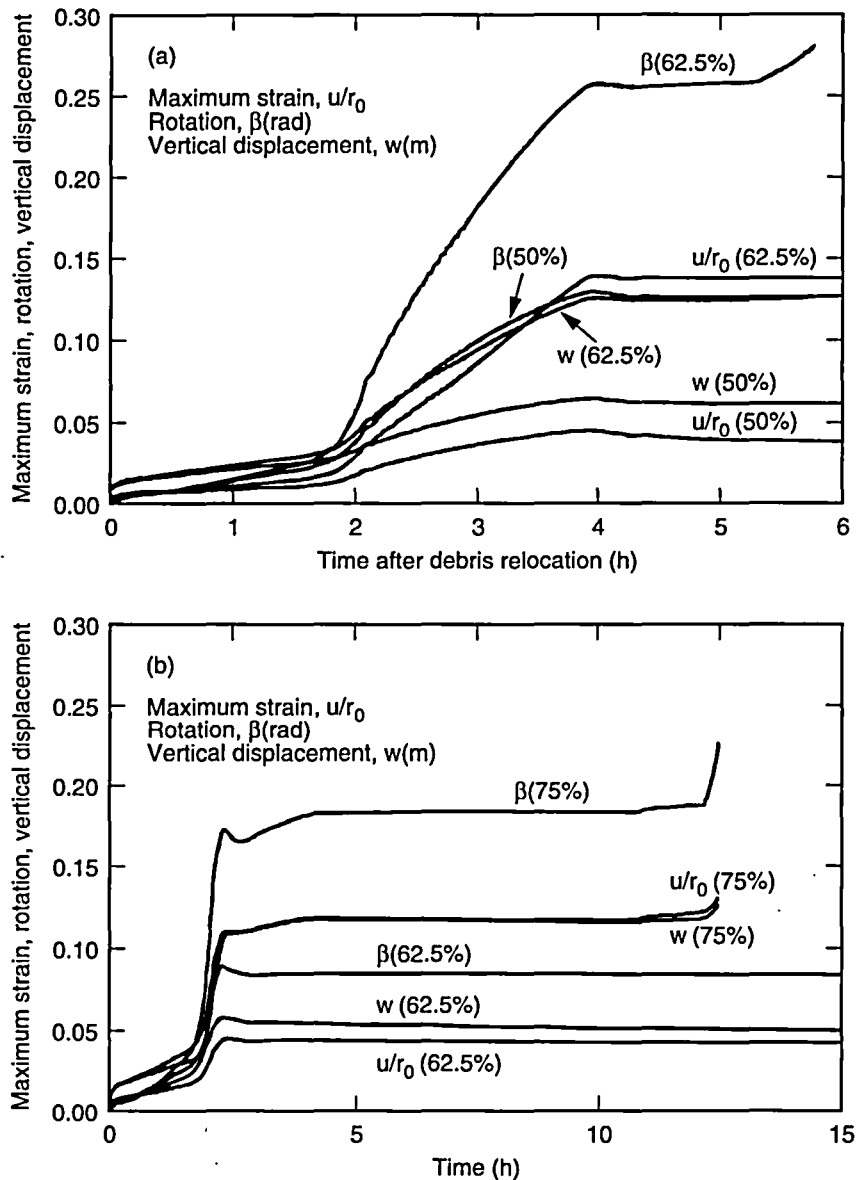


Fig. 8 Results obtained with a deformation-based criterion: (a) slow cooling results and (b) rapid cooling results.

11 hours causes the deformation to increase dramatically. It appears that under rapid cooling hot spots on 75 and 80% of nominal background heat fluxes cause failure in approximately 13 and 11 hours, respectively. Therefore it is concluded that, under rapid cooling conditions and the deformation-based criterion, the vessel can survive a hot spot on a background heat flux between 62.5 and 75% of nominal.

SUMMARY

Data available from the OECD-sponsored TMI-2 VIP, plant instrumentation during the accident, and previous TMI-2 research programs were used to estimate the MTF that existed in the vessel during the accident. These data also provided a unique opportunity to evaluate the predictive capability of severe accident analysis models for

which limited validation data exists. The MTF analysis effort of the VIP included calculations to consider four vessel lower-head failure mechanisms: penetration tube rupture, penetration tube ejection, global vessel rupture, and localized vessel rupture.

Analyses results indicate that tube rupture and tube ejection could be eliminated as potential failure mechanisms during the TMI-2 accident. Global vessel failure analyses suggest that significant debris cooling, not considered in severe accident analysis models, must have occurred within approximately 2 hours after debris relocation to the lower head. Analyses also indicate that additional data are needed to select an appropriate vessel failure criterion because the magnitude of cooling required to obtain vessel temperatures consistent with values inferred from vessel steel examinations was sensitive to the failure criterion used in structural response calculations. Although examinations of companion debris samples did not provide supporting evidence of this additional debris cooling, metallurgical examinations did provide evidence that this cooling occurred in the hot spot location. Localized vessel failure analyses indicate that it is possible for the vessel to withstand the hot spot temperatures for time periods inferred from VIP metallurgical examinations provided that the balance of the vessel is relatively cool. Although there are insufficient data to determine the exact mechanisms that caused the debris to cool, scoping calculation results indicate that a minimal volume of cooling channels within the debris and a minimal size gap between the debris and the vessel could supply the cooling needed to obtain vessel temperatures and cooling rates determined in metallurgical examinations.

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