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entrained from liquid films in annular flow (Andreussi and Azzopardi, 1983) is employed. The envelope of droplet/particle migration determines the boundaries of the mixing zone. The earlier version used a simplified treatment that ignored varia-

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A much improved numerical solution methodology that better accounts for both time and spatial dependencies of the melt stream fragmentation rate, vapor film thickness, and film vapor velocity within the film. The melt stream is now also

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EX-VESSEL MELT-COOLANT INTERACTIONS IN DEEP WATER POOL: STUDIES
AND ACCIDENT MANAGEMENT FOR SWEDISH BWRs

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ABSTRACT

In Swedish BWRs having an annular suppression pool, the lower drywell beneath the reactor vessel is flooded with water to mitigate against the effects of melt release into the drywell during a severe accident. The THIRMA code has been used to analyze the effectiveness of the water pool to protect lower drywell penetrations by fragmenting and quenching the melt as it relocates downward through the water. Experiments have also been performed to investigate the benefits of adding surfactants to the water to reduce the likelihood of fine-scale debris formation from steam explosions. This paper presents an overview of the accident management approach and surfactant investigations together with results from the THIRMA analyses.

I. INTRODUCTION

By the end of 1988, the Swedish Reactor Accident Mitigation Program, initiated after the accident in TMI-2, reached its conclusion. Engineered measures and prepared emergency procedures, aimed at mitigating the consequences of accidents with severe core damage, have been fully implemented at all reactor sites (Höberg, 1988). The program was supported by the research projects FILTRA and RAMA (Reactor Accident Mitigation Analysis) conducted jointly by the Swedish nuclear safety authorities and the utilities. Final reports are now available from the RAMA project.

In order to protect the integrity of BWR containments, the following mitigation measures have been implemented and in operation since the end of 1988 (valid for all BWR plants except Barsebäck 1 and 2 which have had a gravel bed filter vent system since 1985):

- i) Enhanced, high reliability containment spray system;
- ii) Filtered venting to prevent late containment failure;
- iii) Unfiltered venting to prevent early containment overpressurization for accident sequences involving a large LOCA combined with malfunction of the pressure suppression system but operability of the normal ECCS systems. The system consists of a large capacity pressure release line comprising a rupture disc and valves for reclosure after activation. As core melt is not envisaged for this accident sequence, an unfiltered release is considered acceptable.
- iv) Flooding of the lower drywell compartment in BWRs of second generation (i.e., with an annular suppression pool) using the suppression pool water on indication of core damage.

To further explain the last accident management strategy, the second generation of BWRs in Sweden have an annular suppression pool which means that there is no water below the reactor pressure ves-

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sel during normal operation. Special measures are therefore needed to prevent early meltthrough of the containment floor. In a core melt accident, following reactor vessel lower head failure, the molten core materials would relocate into the lower drywell. Electrical and mechanical penetrations in the lower part of the drywell would then be exposed to and most probably damaged by the hot core debris opening a release path from the containment. In order to prevent this from occurring and to provide a means to cool the core debris, a system for flooding the lower drywell has been installed. The system will allow water from the suppression pool to flow by gravity force into the drywell when the isolation valve in the connecting line is opened (Figure 1).

The success of the chosen strategy will depend, to a great extent, on the character and efficiency of the processes of melt fragmentation and quenching in a deep pool of subcooled water. The depth of the water pool in the flooded lower drywell will be between 4 and 12 meters, depending on the particular plant and accident scenario. Steam explosions could negatively affect the coolability of core debris on the containment floor by generation of very small debris particles. The possibility of major destructive steam explosions has been rated remote enough to justify taking advantage of the strategy.

The issues of ex-vessel melt fragmentation, quenching, and core debris coolability have received great attention in the Swedish research (Frid, 1991). Thus, experimental studies were conducted of coolability limits in stratified beds, methods for protecting the penetrations in the lower drywell of BWRs were developed and tested, a method to suppress or mitigate fine-scale fragmentation and steam explosions (to avoid very small debris particles) by adding small amounts of surfactants to the water has been investigated, and calculations of melt stream breakup and debris formation in a deep pool of subcooled water have been performed.

In this paper we describe and discuss the results of experimental studies into the effects of adding surfactants to the water and the results of calculations of melt stream breakup and debris formation using the THIRMAL melt/water interaction code developed at Argonne National Laboratory. However, the surfactant studies are described very briefly as most of the results have been presented and reported earlier (Becker and Lindland, 1989, 1991). It should be noted here that the potential influence of surfactants on the process of melt stream breakup has not been considered in the THIRMAL calculations. It is possible that such an effect exists but it is probably small (Frid, 1991a).

II. SUPPRESSING OF FINE-SCALE FRAGMENTATION AND STEAM EXPLOSIONS BY SURFACTANTS

Becker suggested that fine-scale fragmentation and steam explosions could be mitigated or prevented by adding small amounts of surface active agents (surfactants) to the suppression pool water just prior to vessel meltthrough (Becker and Ölander, 1988). The idea is based on the hypothesis that the surfactant molecules stabilize the water-vapor interface by forming a layer of molecules at the interface.

According to the theory of steam explosions, the destabilization of the vapor film surrounding the melt droplets during the coarse mixing stage is a necessary, but not sufficient, condition for triggering and propagation of an explosion through the melt-water mixture.

The stabilizing effect of surfactants on disturbances at a water-vapor interface is supported by some evidence from the metallurgical and chemical industry (Becker and Ölander, 1988).

In order to explore the potential of using surfactants in severe accident mitigation, the RAMA project has sponsored small-scale experiments at the Royal Institute of Technology (KTH) in Stockholm, and at Sandia National Laboratories (SNL) in the USA. Most recently, the post-RAMA projects HAFOS and APRI (Accident Phenomena of Risk Importance) have sponsored small-scale experiments at the University of Wisconsin in the USA. (HAFOS is a Swedish acronym denoting cooperation on severe accident research between Swedish regulatory authorities and utilities.)

The objective of the experiments at KTH, carried out by Becker and Ölander (1988) was to select the surfactants and to find the optimal surfactant concentration in water for the single droplet experiments at SNL. This objective was achieved by measuring the rise velocity of nitrogen bubbles in water. The measurements were carried out at water temperatures of 20 degrees Centigrade and 50 degrees Centigrade. The surfactant concentrations were varied in the range between 0 and 200 ppm corresponding to the water surface tension in the range of 0.072 and 0.030 N/m. Becker and Ölander found that the behavior of the nitrogen bubbles in water is significantly affected by surfactant concentrations in water of about 1 ppm regardless of the kind of surfactants used (or their mixture).

At this concentration, the oscillations of the bubble surface during the rise were completely eliminated and the bubble rise velocity was significantly decreased. The observed effect of the surfactants was explained by the well-known properties of surface active substances, namely their ability to form a layer of molecules at the

water-vapor interface. According to Becker and Lindland, the stabilization of the gas-water interface is a result of the Marangoni effect.

The effects of small concentrations of surfactants in water on spontaneous and triggered single droplet explosions were investigated at SNL in a joint project with KTH. The study is briefly described by Becker and Lindland (1989, 1991). The spontaneous explosions were studied in a tin-water system using, in most cases, 12 g of tin at 650 degrees Centigrade and ethoxylated - nonyle - fenole (available as Emulgator U-9) solutions. The water temperature was 20, 35, 40 and 50 degrees Centigrade and the surfactant concentration was in the range of 0 to 40 ppm. The triggered explosions were carried out in the thermite-water system for 4.5 g of thermite at 2700 degrees Centigrade and with water at 20 degrees Centigrade. Three experiments were performed for pure water and three experiments with 5 ppm of Emulgator U-9 in water.

Becker and Lindland concluded that the tin-water experiments seemed to support the hypothesis that small concentrations of a surfactant in water mitigate against a steam explosion. However, it should be noted that at the water temperature of 40 degrees Centigrade, an enhancement of the explosion was observed at 5 ppm. With regard to the triggered thermite-water experiments, Becker and Lindland concluded that the surfactant had a strong mitigating influence on the steam explosions.

The mitigating and/or suppressive effects of adding small amounts of surfactants to the water coolant has been investigated in the single droplet explosion tests at the University of Wisconsin-Madison in a joint project with KTH. Molten 0.1 g iron-oxide droplets at about 2000 degrees Centigrade have been used. The water temperature was in the 20 to 50 degrees Centigrade range. The preliminary results from these tests are unclear with indication of the possibility of some suppressive effects.

Obviously, the currently existing database and theoretical foundations on the effects of surfactants in water on steam explosions and melt fragmentation phenomena are insufficient to allow a definite decision to be made as to the application of the proposed method during an accident.

III. ENERGY BALANCE QUENCHING ANALYSIS

It is necessary to establish that the water pool inventory provided beneath the reactor vessel is sufficient to quench the released corium mass. This can be demonstrated by means of a simple energy balance quenching analysis. The analysis was carried out for a water mass associated with the 8.4 meter diameter lower drywell of the Forsmark 3 BWR. For this particular plant, the

water pool depth can vary from 4.1 meters corresponding to the maximum sustainable pressure difference between the drywell and wetwell to 6.9 meters corresponding to no drywell-wetwell pressure differential. The water pool depth was varied over a range of 4 to 12 meters. Greater depths up to 12 meters are also examined to further encompass the depths that may be attained in other Swedish plant designs (e.g., Forsmark 1 and 2). The water pool mass is assumed limited to that in the cylindrical lower drywell cavity beneath the vessel. Interaction of corium with the additional water inside the tunnel passageway that leads from the cylindrical cavity to the airlocks is presently ignored. The containment pressure (0.15 MPa), initial water temperature (321 K), and released melt composition are based upon MAAP 3.0B code calculations for a station blackout (SBO) accident sequence; these conditions are included in Table 1. A mass of 106000 Kg is assumed released representative of roughly half the full core inventory of fuel and structure. A released melt superheat of 50 K is assumed based upon the assessment of Theofanous et. al. (1991) that ascribes the greatest likelihood to the attainment of superheats between 25 and 50 K for sudden melt release from a large (3300 MWt) BWR.

Because the water is initially subcooled by a significant amount (64 K), it is assumed that all steam formed through the quenching process is initially condensed inside the pool such that the water temperature rises until the saturation temperature (385 K) is attained. The mass of melt which has entered the water and has been quenched at the onset of saturation is given by

$$M_{\text{melt,sub}} = M_{\text{water}} \frac{\Delta e_{\text{water,sub}}}{\Delta e_{\text{melt}}} \quad (1)$$

After the water becomes saturated, then quenching of the additional melt will result in vaporization of water giving rise to the formation of steam. Assuming that the steam formed is condensed by the containment pressure suppression systems such that the pressure remains constant, the mass of steam produced by quenching the remainder of the melt is given by

$$M_{\text{steam}} = (M_{\text{melt}} - M_{\text{melt,sub}}) \frac{\Delta e_{\text{melt}}}{\Delta e_{\text{water,vap}}} \quad (2)$$

Assuming that this steam mass is effectively lost from the water pool over the quench timescale, the pool water volume will decrease. The corresponding reduction in pool depth, however, will be diminished by thermal expansion of the remaining water in going from the initial temperature to saturation. The pool height will also tend to rise due to volume displacement by the corium and level swell. However, aside from these latter effects, the final effective water depth is given by

Table 1. CONDITIONS ASSUMED IN ANALYSES

Water Pool Depth, m	6.87
Water Pool Diameter, m	8.4
Water Pool Floor Area, m ²	55.4
Water Pool Volume, m ³	381
Initial Water Temperature, K	321
Containment Pressure, MPa	0.15
Water Saturation Temperature, K	385
Initial Water Subcooling, K	64
Water Pool Mass, Kg	377000
Water Specific Enthalpy Change Between Initial and Saturation Temperature, MJ/Kg	0.270
Water Heat of Vaporization, MJ/Kg	2.227
Water Density, Kg/m ³	
385 K	950
321 K	989
Released Melt Mass, Kg	106000
Released Melt Volume, m ³	14.4
Initial Melt Temperature, K	2723
Initial Melt Superheat, K	50
Melt Composition, wt %	
UO ₂	55.2
ZrO ₂	5.2
Zr	23.3
Stainless Steel	15.5
B ₄ C	0.8
Melt Specific Enthalpy Change Between Initial Temperature and Water Saturation Temperature, MJ/Kg	1.34
Chemical Oxidation Reactions	$\text{Zr} + 2\text{H}_2\text{O} \rightarrow \text{ZrO}_2 + 2\text{H}_2$ $2\text{Cr} + 3\text{H}_2\text{O} \rightarrow \text{Cr}_2\text{O}_3 + 3\text{H}_2$ $0.947\text{Fe} + \text{H}_2\text{O} \rightarrow \text{Fe}_{0.947}\text{O} + \text{H}_2$
Specific Chemical Energy Release from Zirconium Oxidation, MJ/Kg	1.57
Specific Chemical Energy Release from Stainless Steel Oxidation, MJ/Kg	0.205
Combined Specific Chemical Energy Release from Oxidation, MJ/Kg	1.77
Combined Melt Specific Enthalpy Change and Chemical Energy Release, MJ/Kg	3.11
Fall Height from Lower Head to Water Pool, m	6.6
Fall Height from Lower Head to Lower Drywell Floor, m	13.5
Lower Head Wall Thickness, m	0.186
Initial Failure Hole Diameter, cm	6.5
Lower Head Inner Radius, m	3.21
Initial Melt Depth Inside Lower Plenum, m	1.50
Reactor Vessel Pressure, MPa	1.0 to 0.5 ^(a)

- a) Pressure decreases linearly from 1.0 to 0.5 MPa during first 20 seconds following vessel failure and remains at 0.5 MPa until end of release.

$$h = \left(1 - \frac{M_{\text{steam}}}{M_{\text{water}}} \right) \frac{\rho_{\text{water}} (T_{\text{initial}})}{\rho_{\text{water}} (T_{\text{sat}})} h_{\text{initial}} \quad (3)$$

In writing Equation 3, use is made of the fact that expansion of the radially confined water is realized almost entirely in the vertical direction.

Figure 2 shows the corium mass that must be quenched down to the saturation temperature in order to saturate the initially subcooled water. Because of the high proportion of metallic zirconium and stainless steel in the corium, the additional energy release from chemical oxidation reactions is included with the extent of reaction treated as a parameter. It is observed that in the absence of steam formation, complete quenching of the released mass of 106000 Kg will raise the water temperature to saturation except when the pool depth exceeds 9 meters in the limit of negligible oxidation. The net steam mass formed is shown in Figure 3 where it is seen that more than sufficient water is present to completely quench the released melt over the relevant range of water depth. The effects of combined depletion of the water inventory from complete quenching and water thermal expansion upon the pool depth are shown in Figure 4.

The energy balance quenching analysis thus shows that more than sufficient water is available to fully quench the assumed released melt mass of 106000 Kg.

IV. THIRMAL-1 ANALYSIS OF MELT STREAM-WATER INTERACTIONS

A. Modeling Approach

Given that sufficient water is provided in the lower drywell to quench the released mass of molten fuel and structure, the question becomes whether natural physical processes will result in fragmentation and freezing to an extent sufficient to produce a coolable debris state. Analysis of this problem requires the modeling and calculation of a large number of interrelated process. It is therefore not surprising that different researchers in this area have developed diverse calculational capabilities. Sensitivities to basic model assumptions have never been determined in a comprehensive way such that it is difficult to attach uncertainty bounds to the results obtained using any of the current models and codes. The present analysis was carried out using the THIRMAL-1 code that calculates the nonexplosive fragmentation and quenching of a melt stream inside a water pool as well as the associated water heatup, net steam formation, oxidation, and hydrogen generation.

THIRMAL-1 treats the case of a circular melt stream entering the water pool with a time

varying diameter, velocity, temperature, and composition. Except for the initial transient penetration of the melt stream into the pool, the dominant fragmentation mechanism is the erosion of molten droplets from the surface of the melt stream due to the formation of Kelvin-Helmholtz instabilities along the stream surface (Figure 5). Immediately behind the stream leading edge, the instabilities are driven by the upward flow of vapor inside a vapor film immediately surrounding the stream along part of its length. As the eroded fragments enter the surrounding water, heat transfer from the fragments gives rise to the local formation of steam which rises through the pool. This results in the formation of an interaction zone surrounding the stream containing melt droplets, melt particles, water, and steam. The radial extent of the interaction zone reflects the lateral migration of the droplets and particles and their temperatures that directly determine the vapor source.

Near the leading edge, the steam rises through water within the interaction zone in a bubbly flow regime. At increasing heights above the leading edge, the accumulation of steam increases the superficial vapor velocity through the interaction zone causing a transition to a churn turbulent flow regime. Dispersed flow typically develops in the overlying part of the interaction zone. Here, melt droplets and particles together with water droplets are dispersed in the upward flowing steam. Heat transfer from the melt droplets and particles in the dispersed flow region typically produces superheated steam that exits the interaction zone at the elevation of the top of the pool.

Interactions of the upward flowing steam with the melt stream also result in the erosion of droplets from the stream surface through the Kelvin-Helmholtz instability mechanism (Wang, Blomquist and Spencer, 1989, 1989a). Heat transfer to the water droplets and the water at the outer boundary of the interaction zone can occur at such a high rate in the upper part of the dispersed flow region that steam condensation effects are locally suppressed. Depending upon the droplet/particle diameter and the vapor flow conditions, melt droplets and particles in the dispersed region may be swept up out of the pool or may settle downward through a continuous vapor flow before encountering the continuous liquid churn turbulent and bubbly flow regions near the stream leading edge. In the present application, those droplets and particles that are swept out are assumed to rebound elastically off of the steel shield plate located above the pool and to reenter the interaction zone with downward directed momentum.

In addition to Kelvin-Helmholtz instabilities, fragmentation of the melt stream leading edge due to boundary layer stripping is modeled as the stream initially penetrates downward rapidly

through the water pool. However, this initial penetration phase involves only a negligible portion of the released mass in the present analysis. Subsequently, the effects of erosion cause the local stream diameter at the leading edge to decrease to sizes so small that breakup occurs from capillary effects. In the present application, this happens when the diameter locally decreases to about five millimeters. Intermittent capillary breakup at the leading edge is calculated according to a Weber number-based criterion.

The droplet diameter resulting from the Kelvin-Helmholtz mechanism is assumed to be equal to the inverse wavenumber of the fastest growing wavelength locally contributing to the Kelvin-Helmholtz instability. Because the conditions under which erosion occurs vary in space along the melt stream column as well as in time, a distribution of sizes is calculated reflecting the spatial and temporal variations in the conditions under which erosion occurs. The droplet diameter is determined at the instant of erosion from the melt stream. In particular, once a droplet is created, no further breakup of the droplet into smaller sized fragments is modeled. Likewise, no growth in the droplet size as the result of coalescence is modeled.

THIRMAL-1 accounts for the presence of distinct oxide and metal phases in the melt entering the water. In particular, individual droplets eroded from the melt stream are modeled as consisting wholly of either oxide or metal. This approach permits oxide and metal droplets to freeze at different temperatures representing the actual freezing range of each of the individual phases. Modeling distinct freezing transitions is important because the coincident release of oxide and metal at the same temperature will result in the metal having a much higher molten superheat above its liquidus temperature than the oxide. Thermophysical properties assumed for the oxide and metal phases as well as relevant properties for the heterogeneous oxide-metal melt mixture are shown in Table 7. In the present analysis, erosion of the melt stream and droplet formation are calculated using the heterogeneous mixture properties. In particular, the erosion rate and droplet diameter reflect the mixture surface tension. As a consequence, oxide and metal droplets formed at the same location along the column and at the same time are assumed to have the same diameter.

THIRMAL-1 models oxidation of the metallic droplets and particles as they relocate through the interaction zone and water pool. Oxidation of metal when it is still part of the melt stream is not calculated. Oxidation rate limitations from both steam diffusion through a vapor/hydrogen film or the dispersed flow continuum and parabolic ion diffusion through the droplet/particle itself are modeled. Reaction of zirco-

nium and the stainless steel constituents is assumed to occur sequentially. The oxidation energy released is deposited in the droplet/particle as a heat source.

An approximate assumption made in THIRMAL-1 is that the water pool depth remains constant in time. Thus, the effects of water depletion resulting from net steam formation that would lower the water level are ignored. On the other hand, heatup of subcooled water is accompanied by thermal expansion of the water which is also not modeled. The resulting volume expansion of the radially confined water will be mainly realized as an increase in the water level tending to somewhat offset the effects of net steaming. A second approximation in THIRMAL-1 is to ignore the water pool heatup and net steam formation associated with heat transfer off of the debris that collects and spreads on the lower drywell floor. Thus, THIRMAL-1 presently predicts effects associated solely with melt fall stage relocation through the water pool.

B. Model Application

THIRMAL-1 was applied to the case of melt stream-water pool interactions during a station blackout sequence in the Forsmark 3 plant. Lower head failure is assumed to involve a single instrument/neutron flux measurement tube or tube penetration resulting in melt release through an initial 6.5 cm diameter pathway through the lower head wall. The water depth at the beginning of melt release is taken to be 6.9 meters. A melt mass of 106000 Kg is assumed released.

The RPV-to-drywell pressure difference was obtained from MAAP 3.0B. In particular, the pressure difference decreases from 0.85 to 0.35 MPa over the first 20 seconds following head failure and thereafter remains at 0.35 MPa until the release phase is finished. Other conditions are summarized in Table 1. The time dependent diameter and velocity of the stream exiting the lower head were calculated using a detailed melt release/hole erosion model that accounts for spatial variations in the thermal erosion-induced enlargement of the hole through the thickness of the lower head wall. These spatial variations arise from the local variations in the forced convection heat flux from the flowing melt. Gravitational acceleration and contraction of the stream as it falls to the water pool upper surface are significant and accounted for in calculating the melt entry conditions. This model is described in greater detail in Appendix A.

Thermophysical properties assumed in the analysis are contained in Tables 2 and 3.

The melt is assumed to relocate from the lower head to the water surface as a coherent circular stream. Interactions of the stream with shielding or CRD-related structures beneath the

RPV are currently neglected. Together with the assumption that the release involves only a single instrument tube, this is expected to represent a conservative assumption. In particular, these assumptions restrict all the melt to enter the water as a single stream and tend to increase both the entry diameter and velocity. This is expected to enhance the calculated melt stream penetration distance below the water surface and the potential for debris to collect on the floor in a molten state in the calculations.

Figures 6 through 9 show the calculated time dependent conditions of melt release from the lower head and melt entry into the water pool. The release phase is calculated to last 106 seconds during which the hole/stream diameter at the head increases from 6.5 to 17 cm. The release velocity decreases from 16 to 10 m/s. The corresponding melt stream entry velocity decreases from 20 to 16 m/s during the first 20 s and continues to decrease to 15 m/s thereafter. The stream entry diameter rises from 5.9 to 14 cm over the release phase duration.

Figure 10 shows the calculated time dependent penetration of the melt stream leading edge. The location of the leading edge is calculated to vary in an oscillatory manner. This is a result of the modeling assumption that allows the portion of the melt stream immediately behind the leading edge to break up according to a Weber number based criterion when the diameter becomes small. In the present application this happens when the stream diameter decreases to about 5 millimeters. Leading edge breakup suddenly decreases the length of the stream by a finite amount. However, because the stream now undergoes erosion over a somewhat shorter length and lesser surface area, the stream leading edge penetrates further downward into the pool. When the diameter of the new leading edge decreases sufficiently to break up, then the cycle is calculated to begin again.

After 25 seconds, the melt stream is calculated to intermittently impinge upon the lower drywell floor. Thus, a portion of the melt is calculated to arrive at the floor in a molten state. However, this is only a minor amount of the released corium mass as observed from Figure 11. By 115 seconds, nearly all of the corium is calculated to have settled out upon the floor. At this time, 94000 of the 106000 Kg released has collected as a particle bed. This corresponds to 89 percent of the collected corium mass (Figure 12).

The energy removed from the particles during quenching is mainly realized in the heatup of the water pool. Figure 13 shows the mean water pool temperature which is calculated to rise by 44 degrees Kelvin due solely to fall stage quenching of the melt. However, the water remains subcool-

ed due to the high initial 64 degree Kelvin subcooling. A difference between the assumptions of the energy balance quenching analysis and the present results is that THIRMAL-1 calculates that superheated steam exits the pool while it is still in a global state of water subcooling.

The calculation also indicates that a large fraction of sub-millimeter sized particles could be formed and that a large amount of hydrogen (e.g., 1000 Kg) could be produced. However, these particular indications must currently be viewed as preliminary and require further investigation.

In summary, the THIRMAL-1 calculation for a SBO sequence predicts that 89 percent of the melt settles out as fully solidified particles together with 9 percent that impinges as a molten stream and 2 percent that collects as molten or partially frozen droplets.

V. SCOPING SENSITIVITY ANALYSIS

Prior to the conduct of the time dependent THIRMAL-1 calculations, THIRMAL-0 was used to scope the melt release and melt-water interaction behavior, and to investigate the sensitivities to the accident sequence, initial melt superheat, and total released melt mass. THIRMAL-0 (Wang, Blomquist and Spencer, 1989; 1989a; Sienicki, Wang and Spencer, 1992) was an earlier version of THIRMAL; differences between THIRMAL-1 and THIRMAL-0 are summarized in Appendix B. Because THIRMAL-0 did not treat the case of time varying melt entry conditions, the code was used to calculate the melt-water interaction behavior at snapshots in time during the release process. In particular, those times when 50 and just less than 100 percent of the cumulative melt mass have entered the water were selected. Assuming constant entry diameters calculated at these particular times, THIRMAL-0 was run until a quasi-steady solution was obtained. Because a full time dependent calculation was not performed, the effects of progressive heatup of the water were neglected such that the pool subcooling was assumed to remain unvarying over the release duration. In the previous section, the water temperature is calculated to rise by no less than 44 K when a mass of 106000 Kg enters the pool as a coherent melt stream.

To determine melt entry conditions, the scoping analysis did not incorporate the detailed multinode hole erosion model discussed in Appendix A. Instead, a simple single node erosion calculation was employed. The heat transfer coefficient was defined to be the maximum of that of Dittus and Boelter for fully developed pipe flow,

$$h_{\text{conv}} = 0.023 \text{ Re}_{\text{hole}}^{0.8} \text{ Pr}^{0.4} \frac{k}{D_{\text{hole}}}, \quad (4)$$

and a rough approximation of an entrance coefficient given by

$$h_{conv} = 2 (Re_L Pr)^{1/2} \frac{k}{L_{head}} \quad (5)$$

The resulting hole and stream diameters calculated for the scoping analysis exceed those predicted with the more detailed model incorporated in THIRMAL-1 (Appendix A). In this regard, the larger melt entry diameters used in the scoping analysis are regarded as a conservative assumption. Another assumption concerns the melt entry velocity. This was not taken to be the calculated entry velocity at the 50 and 100 percent release times. Rather, the velocity exiting the head for the 50 percent cumulative release calculation was defined as that corresponding to the mean release rate over the first half of the release divided by the hole area at the 50 percent release time. Similarly, the velocity exiting the lower head for the 100 percent cumulative release calculation was taken to be that corresponding to the mean release rate over the second half of the release divided by the final hole area. The melt entry velocity is not strongly sensitive to this assumption due to the significant incremental velocity rise associated with the 6.6 m fall height from the lower head to the water pool surface.

Melt thermophysical properties assumed in the scoping analysis are presented in Table 4.

The definition of cases and results are shown in Tables 5 and 6, respectively. It is observed that for the LOCA cases (in which melt gravity drains from the lower plenum) the behavior involves melt steam fragmentation and freezing prior to collection upon the lower drywell floor. The melt stream diameter and breakup length calculated for the LOCA sequences are observed to be sensitive to the initial melt superheat.

For the SBO with an assumed initial molten superheat of 5 K, complete breakup and freezing are also calculated. However, when the initial superheat is increased to 50 K and the full core inventory is assumed released (i.e., 212000 Kg), some of the corium entering with the final stream diameter is calculated to arrive upon the floor in an unsolidified state. Forty-three percent of the corium entering the water at the very end of the release phase is calculated to collect as molten or partially frozen droplets together with two percent impinging as a molten stream that does not break up completely prior to reaching the floor. The remaining 55 percent settles out as fully solidified particles. In contrast, melt entering with the smaller diameter characteristic of the 50 percent cumulative release time is predicted to fully fragment and freeze. This suggests that the release of lower total masses somewhat below 212000 Kg would result in the

calculation of particle bed formation throughout the release duration. This was demonstrated by Case 5 in which the released mass was reduced to half the core inventory.

The released melt mass, initial superheat, and composition for Case 5 are identical to those of the THIRMAL-1 calculation described in the previous section. This case thus serves to delineate differences in the predictions of the two code versions. One major difference already noted is that the THIRMAL-0 melt release/hole erosion model predicted larger melt stream diameters (e.g., 0.25 m at the end of the release) than the more detailed model incorporated into THIRMAL-1 (0.14 m at the end of release). Even though the stream diameters entering the water are significantly smaller, longer melt stream penetrations and a greater extent of melt collection in a molten state are calculated with THIRMAL-1. This indicates that the breakup/penetration lengths and extents of molten corium collection were underestimated in the scoping sensitivity analysis.

The scoping sensitivity calculations thus serve to reveal a sensitivity to the released melt superheat mainly through its effect on the stream diameter as well as a sensitivity to the accident sequence. In particular, the SBO sequence represents a more severe case than the LOCA with regard to melt fragmentation because of the higher melt stream velocities resulting from the RPV-to-drywell pressure difference and the lower drywell absolute pressure.

VI. CONCLUSIONS

A final conclusion with regard to the feasibility of using surfactants to mitigate ex-vessel steam explosions and fine-scale fragmentation cannot be reached on the basis of knowledge available thus far. A critical review of this subject will be made as soon as the experiments at the University of Wisconsin are evaluated. Decision as to the continuation of the research in this area will be made following this evaluation. One possibility which has been discussed is to investigate the effectiveness of surfactants in experiments involving larger melt masses.

The results of the THIRMAL calculations, which were chosen to include what are believed to be a number of conservatisms, raise some concern with regard to the effectiveness of the water pool in the lower drywell in Forsmark 3 to completely disintegrate and quench the molten stream prior to arrival on the containment floor. One important question in this context - besides of course the question of how good are the THIRMAL predictions - is how conservative are the calculations presented in this paper. This is a difficult problem since the initial and boundary conditions in the THIRMAL calculations are functions of the in-vessel accident progression

where the uncertainties are large. However, the assumption that one hundred metric tonnes of melt is available for release during a short period of time is likely to be conservative. Also, neglecting structures below the reactor pressure vessel is conservative, at least during the initial phase of melt release. With regard to the other important parameters, namely melt superheat and release rate, it is more difficult to make a judgement; it is possible that the values used in present calculations are not conservative.

The existing uncertainties and the importance of effective melt quenching for accident mitigation motivate continued research in this area. A need exists for experimental validation of the THIRMA-1 code as well as other codes for melt-water interactions.

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NOMENCLATURE

C	= specific heat, J/(Kg·K)
C'_{fx}	= skin friction coefficient
D	= diameter, m
g	= gravitational acceleration, m/s ²
H_{fall}	= fall distance from bottom of lower head to water pool upper surface, m
h	= depth, m
h_{conv}	= forced convection heat transfer coefficient, W/(m ² ·K)
k	= thermal conductivity, W/(m·K)
L_{head}	= lower head wall thickness, m
M_{melt}	= released melt mass, Kg
$M_{melt,sub}$	= mass of melt quenched that removes water subcooling, Kg
M_{steam}	= net steam mass formed, Kg
M_{water}	= total water mass, Kg
Nu	= Nusselt number
	= $\frac{h_{conv} D}{k}$

Nu_x	= Nusselt number based on x,
	= $\frac{h_{conv} x}{k}$
Pr	= Prandtl number
	= $\frac{C\mu}{k}$
Re	= Reynolds number
	= $\frac{\rho UD}{\mu}$
Re_{hole}	= $\frac{\rho U_{hole} D_{hole}}{\mu}$
Re_L	= $\frac{\rho U_{hole} L_{head}}{\mu}$
Re_x	= Reynolds number based on x
	= $\frac{\rho Ux}{\mu}$
$T_{initial}$	= initial water temperature, K
T_{sat}	= water saturation temperature, K
t	= time, s
U	= velocity, m/s
U_{hole}	= velocity of melt exiting at bottom of lower head wall, m/s
V	= melt volume inside lower plenum, m ³
x	= distance coordinate into channel, m
x^+	= $\frac{2 \left(\frac{x}{D} \right)}{Re Pr}$
Δe_{melt}	= difference in melt specific enthalpy between initial melt temperature and water temperature, J
Δe_s	= change in specific enthalpy of lower head steel in going from initial temperature to liquidus, J
$\Delta e_{water,sub}$	= difference in water liquid phase specific enthalpy between saturation and initial temperatures, J
$\Delta e_{water,vap}$	= water heat of vaporization, J
ΔT_{sup}	= molten superheat of melt above oxide phase liquidus temperature, K
μ	= viscosity, Kg/(m·s)

- ρ = density, Kg/m³
- ent = subscript denoting melt entry conditions at water surface
- hole = subscript denoting conditons at hole through lower head wall

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APPENDIX A

MODELING OF MELT RELEASE FROM THE LOWER PLENUM, LOWER HEAD WALL HOLE ENLARGEMENT, AND MELT ENTRY CONDITIONS

Lower head failure is assumed to occur in a localized fashion as the result of formation of a single flowpath for melt flow through the lower head wall thickness. In the current analysis, the formation of a hole through the head wall is assumed to involve failure of a single instrument/neutron flux measurement tube or tube penetration. It is further assumed that a melt pool has collected to its full depth upon the lower head at the time of failure.

The velocity of melt flowing from the lower head is assumed to be given by

$$U_{\text{hole}} = \left[\frac{2(\rho g h + \Delta P)}{\rho} \right]^{1/2} \quad (\text{A1})$$

and the melt volume inside the lower plenum decreases with time as

$$\frac{dV}{dt} = -U_{\text{hole}} \frac{\pi}{4} D_{\text{hole}}^2 \quad (\text{A2})$$

The melt volume is related to the melt depth through a functional relationship specific to the lower plenum configuration and dimensions.

At the assumed molten superheats, melt flowing through the opening in the lower head wall will heat the surrounding lower head steel up to the melting temperature within a fraction of a second. Subsequently, the surrounding steel will be eroded away and the hole diameter enlarged

with time due to forced convection heat transfer from the superheated melt flowing past the steel surface. The increase in the local hole diameter with time is calculated from the equation,

$$\frac{dD}{dt} = \frac{2h_{\text{conv}} \Delta T_{\text{sup}}}{\rho_s \Delta e_s} \quad (\text{A3})$$

The erosion rate represented by Equation A3 assumes that solid crusts of frozen oxidic material are present between the flowing melt and the steel wall substrate. For a corium flow containing molten oxide and having a superheat equal to the value assumed in the current study, a crust will form upon the carbon steel inner surface of the hole penetrating the lower head thickness. Following the inception of steel melting, the melted steel is subsequently expected to flow downward as a molten film between the corium and underlying solid steel. In this manner, the melted steel is removed and the hole enlarged in diameter. While steel melting is taking place, a crust will still be present again providing a temperature boundary condition for heat transfer. In this situation, the crust is expected to exist as islands that form and move on the melted steel wall film.

As melt drains from the lower plenum, fresh melt is continually transported over the eroding interface. The oxide material that freezes out to form the crusts is expected to have a composition that corresponds to freezing at the oxide phase liquidus temperature. The appropriate temperature difference for forced convection heat transfer is therefore the difference between the bulk melt temperature and the oxide phase liquidus temperature. This is identically equal to the molten superheat of the melt.

In addition to the temperature difference for heat transfer, an important component of Equation A3 is the selection of an appropriate forced convection heat transfer coefficient. Melt is expected to approach the hole through the lower head wall in a laminar flow regime. However, as the melt accelerates and flows through the hole, the velocities become high enough for local turbulence to develop in the vicinity of the eroding interface. The heat transfer coefficient is therefore largely based upon flow over a flat plate. This approach is employed in the analysis of entrance region heat transfer at high Reynolds numbers. In the present instance, it is noted that the diameter of the hole may exceed the thickness of the lower head wall.

The forced convection heat transfer coefficient for flow over a flat plate is a function of the distance along the plate. The present evaluation predicts an initial laminar flow region followed by a transition to turbulent heat transport. In the laminar section, the flat plate heat transfer

coefficient is given by the Nusselt number relationship (Schlichting, 1979),

$$Nu_x = 0.332 Re_x^{1/2} Pr^{1/3} \quad (\text{A4})$$

It is convenient to rewrite Equation A4 in terms of Nusselt and Reynolds numbers based on the hole diameter,

$$Nu = \frac{0.332}{Pr^{1/6}} \left(\frac{2}{x^+} \right)^{1/2} \quad (\text{A5})$$

When melt release begins, the instrument tube diameter is less than the lower head wall thickness. Recognizing that the flow may have aspects of tube entrance flow, the correlation (Gnielinski, 1989),

$$Nu = \begin{cases} 1.077 \left(\frac{2}{x^+} \right)^{1/3}, & \text{if } x^+ < 0.02 \\ 3.66, & \text{if } x^+ \geq 0.02, \end{cases} \quad (\text{A6})$$

appropriate for thermally developing laminar flow in a tube with a constant wall temperature is also considered. The release model selects the maximum laminar heat transfer coefficient between Equations A5 and A6. For the conditions of the current study, Equation A5 typically provides a greater heat transfer coefficient.

The flow in a layer immediately adjacent to the wall becomes turbulent and turbulent heat transfer begins at a distance, x_{crit} , where the Reynolds number

$$Re_{x,\text{crit}} = \frac{\rho U x_{\text{crit}}}{\mu} = 500000. \quad (\text{A7})$$

In the turbulent regime, the Nusselt number correlation of von Kármán (Schlichting, 1979) is used whereby

$$Nu_x = \frac{\frac{1}{2} Re_x C_{fx}}{1 + 5 \left(\frac{1}{2} C_{fx} \right)^{1/2} \left\{ Pr - 1 + \ln \left[1 + \frac{5}{6} (Pr - 1) \right] \right\}} \quad (\text{A8})$$

where

$$C_{fx} = \frac{0.0576}{Re_x^{1/5}} \quad (\text{A9})$$

The heat transfer coefficient depends upon the distance, x , downward into the hole. It follows that the local erosion rate of the head wall and the local diameter are functions of the distance into the hole. For this reason, the release model solves Equation A3 at a user specified number of locations through the head wall

thickness. In the present analysis, the time dependent diameter was calculated at twenty points through the head thickness.

The "hole diameter" in Equation A2 and the diameter of the released melt stream correspond to the minimum diameter calculated through the head thickness at any given time. The stream diameter thus tends to reflect more the erosion rate corresponding to the minimum heat transfer coefficient over the head thickness. The location of the minimum will be either: (i) the place where the transition from laminar to turbulent flow occurs with the minimum heat transfer coefficient given by the appropriate laminar expression; (ii) the bottom of the lower head with the heat transfer coefficient given by the turbulent expression; or (iii) the bottom of the lower head with the coefficient given by the appropriate laminar expression in the absence of the development of turbulence. After draining through the hole in the lower head wall, the melt stream will accelerate and contract under the influence of gravity. The melt stream velocity entering the water pool is thus given by

$$U_{ent} = (U_{hole}^2 + 2g H_{fall})^{1/2} \quad (A10)$$

and the stream water entry diameter is obtained from the mass conservation relationship,

$$D_{ent} = D_{hole} \left(\frac{U_{hole}}{U_{ent}} \right)^{1/2} \quad (A11)$$

APPENDIX B

DIFFERENCES BETWEEN THIRMAL-1 AND THIRMAL-0 MODELS

Both the THIRMAL-1 and THIRMAL-0 codes have been used in carrying out the various analyses. THIRMAL-0 was the original version and has been described previously (Wang, Blomquist and Spencer, 1989; 1989a; Sienicki, Wang and Spencer, 1992). THIRMAL-1 represents a significantly improved version incorporating a number of recent modifications. The most important improvements that distinguish THIRMAL-1 from THIRMAL-0 are:

- i) An extension to handle time dependent conditions of melt stream entry into the water, specifically time varying jet diameter, velocity, temperature, and composition. For example, the erosion of the hole through the lower head gives rise to a diameter that increases significantly with time. Concurrently, the velocity of the stream exiting the hole decreases with time as the melt depth inside the lower plenum decreases, especially during a LOCA sequence. The previous version required the definition of fixed (i.e., unvarying with time) melt entry conditions.

- ii) An extension to treat the presence of distinct oxide and metal phases. This extension encompasses situations in which the melt is released in a stratified state (e.g., all the metal is released first followed by all of the oxide) or the melt stream consists of a heterogeneous oxide-metal mixture (i.e., the oxide and metal are released together). The extent of mixing of oxide and metal is assumed to remain at such a level that individual droplets resulting from melt stream fragmentation are modeled as either all oxide or all metal in the new version. This is important because the metal freezes at a much lower temperature than the oxide. In the previous version, the melt was modeled as a homogeneous material having a single freezing temperature.

- iii) An improved and more physical treatment of the hydrodynamic and heat transfer phenomena taking place in the mixing region containing melt droplets/particles, water, and steam. The one-dimensional vapor mass and energy equations were reformulated to enable an improved prediction of vapor void fractions and temperatures on the Eulerian pool grid. Local flow regimes within the zone are determined from a flow regime map that includes bubbly, churn turbulent, and dispersed regimes. Transitions between the different regimes are predicted using recognized criteria involving the appropriate Kutateladze group containing the superficial vapor velocity. The previous version modeled only bubbly and dispersed flow assuming a transition between the two at a fixed void fraction of 0.75. The new version solves a quasi-steady vapor momentum equation to obtain vapor velocities in dispersed regions whereas the old version assumed a constant dispersed flow steam velocity. The new version also incorporates improved modeling for heat transfer controlled vaporization/condensation of bubbles in the bubbly and churn turbulent regimes as well as for flowing vapor in dispersed flow regions. The modeling of the steam generation rate associated with heat transfer from melt droplets and particles in the mixing zone has been made more realistic and dependent upon the local flow regime and conditions.

- iv) The Lagrangian treatment of droplet/particle relocation has been improved to account for drag effects associated with local conditions (e.g., void fraction) in the mixing zone and to provide a better representation of the lateral velocity with which fragments come off of the melt stream column. In particular, a correlation based upon the velocities with which droplets are

entrained from liquid films in annular flow (Andreussi and Azzopardi, 1983) is employed. The envelope of droplet/particle migration determines the boundaries of the mixing zone. The earlier version used a simplified treatment that ignored variations in local conditions and assumed high lateral velocities for eroded droplets.

- v) Determination of the maximum Kelvin-Helmholtz wave growth rate and the corresponding wave number using a wave number spectrum analysis. The maximum growth rate and associated wave number are used to define the stream column erosion rate and fragment size. Previously, a most probable wave number was obtained from a simple approximate expression that ignored the effects of the vapor film thickness. The new approach could be further extended to predict a distribution of eroded fragment sizes representing a range of wave numbers.

- vi) A much improved numerical solution methodology that better accounts for both time and spatial dependencies of the melt stream fragmentation rate, vapor film thickness, and film vapor velocity within the film. The melt stream is now also modeled in terms of Lagrangian cells that move through a fixed Eulerian grid in the pool. The previous version resolved the stream on the fixed Eulerian grid such that step changes in stream penetration and length were computed rather than a continuous penetration versus time.

- vii) An extension to treat the time dependent pressurization of a closed volume system and the heatup of the water pool. The system pressurization and pool heatup models in the previous version were incomplete.

- viii) The calculation of erosion-induced enlargement of the hole through the lower head wall and melt release from the lower plenum has been improved (Appendix A).

Table 2. CORIUM THERMOPHYSICAL PROPERTIES ASSUMED IN THIRMA-1 ANALYSIS

Property	Oxide Phase	Metal Phase	Mixture
Liquidus Temperature, K	2673	2173	-----
Solidus Temperature, K	2613	2093	-----
Specific Enthalpy, MJ/Kg			
Liquidus	1.28	1.18	-----
Solidus	0.892	0.917	-----
298 K	0	0	-----
Liquid Specific Heat, KJ/(Kg·K)	0.513	0.563	0.534
Solid Specific Heat, KJ/(Kg·K)	0.385	0.511	0.479
Liquid Density, Kg/m ³	8370	6110	7320
Liquid Thermal Conductivity, W/(m·K)	3.4	31	9.6
Viscosity, Pa·s	4.2x10 ⁻³	2.1x10 ⁻³	3.7x10 ⁻³
Surface Tension, N/m	0.52	1.60	1.02
Emittance	0.83	0.39	0.62

Table 3. LOWER HEAD WALL CARBON STEEL THERMOPHYSICAL PROPERTIES ASSUMED IN ANALYSIS

Property	Value
Freezing/Melting Temperature, K	1811
Specific Enthalpy, MJ/Kg	
Liquidus	1.29
Solidus	1.05
298 K	0
Solid Specific Heat, KJ/(Kg·K)	0.761
Solid Density, Kg/m ³	7320

Table 4. CORIUM THERMOPHYSICAL PROPERTIES USED IN SCOPING SENSITIVITY ANALYSIS

Property	Property Value
Freezing/Melting Temperature, K	2500
Specific Enthalpy, MJ/Kg	
Liquidus	1.30
Solidus	1.01
298 K	0
Liquid Phase Specific Heat, KJ/(Kg·K)	0.554
Solid Phase Specific Heat, KJ/(Kg·K)	0.459
Liquid Density, Kg/m ³	7250
Liquid Thermal Conductivity, W/(m·K)	17
Viscosity, Pa·s	3.62×10^{-3}
Surface Tension, N/m	0.72

Table 5. CONDITIONS OF SCOPING SENSITIVITY ANALYSIS^(a)

Case No.	Accident Sequence	Released Melt Mass, Kg	Initial Melt Superheat, K	RPV Pressure, MPa	Containment Pressure, MPa	Initial Water Temperature, K	Initial Water Subcooling, K
1	LOCA	212000	5	0.43	0.43	321	98
2	LOCA	212000	50	0.43	0.43	321	98
3	SBO	212000	5	1.0 to 0.5 ^(b)	0.15	321	62
4	SBO	212000	50	1.0 to 0.5 ^(b)	0.15	321	62
5	SBO	106000	50	1.0 to 0.5 ^(b)	0.15	321	62

a) Water depth = 6.87 m.

b) Pressure decreases linearly from 1.0 to 0.5 MPa during first 20 seconds following vessel failure and remains at 0.5 MPa until end of release.

Table 6. RESULTS OF SCOPING SENSITIVITY ANALYSIS

Case No.	Total Release Phase Duration, s	Cumulative Mass Entry into Water, %	Melt Stream Diameter Exiting Lower Head, m	Melt Stream Diameter at Water Surface, m	Melt Stream Velocity at Water Surface, m	Melt Stream Breakup Length, m	Corium Arrival State Particles/Droplets/Stream, %
1	485	50 100	0.14 0.17	0.075 0.090	11.9 11.9	3.3 3.6	100/0/0 100/0/0
2	138	50 100	0.28 0.34	0.13 0.18	11.7 11.9	4.1 5.3	100/0/0 100/0/0
3	174	50 100	0.16 0.19	0.11 0.15	13.1 14.5	4.2 5.4	100/0/0 100/0/0
4	44	50 100	0.32 0.39	0.22 0.31	12.9 14.4	5.2 >6.9	100/0/0 55/43/2
5	38	50 100	0.26 0.31	0.18 0.25	13.1 14.4	4.9 6.2	100/0/0 100/0/0

Figure 1. Schematic Illustration of Forsmark 3 BWR Containment.

Figure 2. Corium Mass that Must be Completely Quenched to Saturate Lower Drywell Water Pool Initially Subcooled by 64 K.

Figure 3. Net Steam Mass Formed from Complete Quenching of 106000 Kg of Corium in 64 K Initially Subcooled Lower Drywell Water Pool.

Figure 4. Water Pool Depth Following Complete Quenching of 106000 Kg of Corium in 64 K Initially Subcooled Lower Drywell Water Pool.

Figure 5. Schematic Illustration of Melt Stream Breakup from Kelvin-Helmholtz Instabilities Modeled in THIRMAL-1.

Figure 6. Melt Stream Diameter and Velocity Exiting Lower Head.

Figure 8. Cumulative Released Melt Mass and Melt Depth Remaining Inside Lower Plenum.

Figure 7. Melt Velocity Exiting Lower Head and Melt Depth Remaining Inside Lower Plenum.

Figure 9. Melt Stream Diameter and Velocity Entering Surface of Water Pool.

Figure 10. Penetration of Leading Edge of Coherent Melt Stream Column.

Figure 12. Cumulative Corium Relative Collection at Lower Drywell Floor.

Figure 11. Cumulative Corium Collection at Lower Drywell Floor.

Figure 13. Water Pool Heatup Due Solely to Melt Fall Stage Quenching.

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