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The Probability of Mark-I Containment Failure by Melt-Attack of the Liner

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**PART I: THE PROBABILITY OF MARK-I CONTAINMENT
FAILURE BY MELT-ATTACK OF THE LINER:
THE PROBABILISTIC SYNTHESIS**

Abstract

This report is a followup to the work presented in NUREG/CR-5423 addressing early failure of a BWR Mark I containment by melt attack of the liner, and it constitutes a part of the implementation of the Risk-Oriented Accident Analysis Methodology (ROAAM) employed therein. In particular, it expands the quantification to include four independent evaluations carried out at Rensselaer Polytechnic Institute, Argonne National Laboratories, Sandia National Laboratories and ANATECH, Inc. on the various portions of the phenomenology involved. These independent evaluations are included here as Parts II through V. The results, and their integration in Part I, demonstrate the substantial synergism and convergence necessary to recognize that the issue has been resolved.

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1.0 Introduction and Background

This report is written as a sequel to NUREG/CR-5423, and together they are intended to address and resolve the so-called "Mark I Liner Attack" issue. NUREG/CR-5423 represents the first step in the Risk Oriented Accident Analysis Methodology (ROAAM) employed for this purpose, while the present report is intended to represent the last step. In particular, in NUREG/CR-5423 one can find:

- a complete definition of the problem;
- a description of the methodology;
- the probabilistic framework employed;
- a quantification, including use of all prior work;
- the results of the probabilistic synthesis, i.e., the overall quantification;
- two rounds of comments by experts in a 19-member peer review panel; and
- authors' responses.

Also in NUREG/CR-5423 one can find the initiation of a second step in the ROAAM process: that of focusing the technical issues raised during the review into four areas requiring further, and independent, evaluation. This focusing took place through the work of four working groups (including members not in the original panel) and is documented in NUREG/CR-5423 (Appendix L) by respective summaries of the work and sets of conclusions and recommendations. The actual second step took place in implementing these recommendations which resulted in four independent research/audit efforts (to be described shortly below). The third step, initiated by the present report, involves the use of the results of these independent evaluations into an updated quantification, and is expected to be completed through the involvement of a peer review panel. The four independent evaluations are documented in Parts II through V of this report. Each of these parts has been reviewed by the members of

the respective working group that recommended the work, and the results of these reviews are documented in Appendix I to this report. The results of the review of this final report, as a whole, are documented in Appendix II.

To avoid unnecessary duplication, this report is designed to be read in conjunction with NUREG/CR-5423. The connection is made by the very brief remarks in the next two paragraphs, one devoted to the technical approach and the other to the conclusions and recommendations of the four working groups.

Very briefly, the problem is concerned with the possibility that the molten corium released from the reactor vessel (in a low-pressure unmitigated severe accident in a BWR with a Mark I pressure suppression containment) will come in contact with the containment liner and cause a breach in it shortly thereafter. This liner is the containment pressure boundary, and such a breach would constitute an "early" containment failure. The situation is illustrated in Figure 1.1. The important phenomenology is sketched in Figure 1.2, which also notes the key terminology employed in such discussions. The objective is to determine the likelihood of such a liner breach (conditional on the occurrence of an unmitigated severe accident), and especially to consider the effect of flooding (by water) of the drywell floor. The problem was decomposed in the manner illustrated in Figure 1.3, along with the definition of several melt release scenarios for consideration in the manner illustrated in Figure 1.4. The quantification (of the pdf's and the CRs in Figure 1.3) employs a probability scale, and the results, summarized in Table 1.1, interpreted by the same scale (see Table 1.2) indicate that in the presence of water such liner failure is "physically unreasonable."

Figure 1.1. Illustration of the Mark-I liner attack problem.

Figure 1.2. Illustration of the phenomenology involved in the Mark-I liner attack problem

Figure 1.3. The probabilistic framework for addressing Mark-I liner attack.

Figure 1.4. The enveloping, splinter scenarios considered.

Table 1.1. The Main Results of NUREG/CR-5423
Numbers to be Interpreted on the Basis of the Probability Scale Shown in Table 1.2.

	Scenario I	Scenario II
Without Water	1	0.63
With Water	6×10^{-5}	1.2×10^{-4} or 3.5×10^{-6}

Table 1.2. Definition of Probability Levels

Process Likelihood	Process Characteristics
1/10	Behavior is within known trends but obtainable only at the edge-of-spectrum parameters.
1/100	Behavior cannot be positively excluded, but it is outside the spectrum of reason.
1/1000	Behavior is physically unreasonable and violates well-known reality. Its occurrence can be argued against positively.

The four areas addressed by the working groups and respective recommendations can be summarized as follows:

1. Melt Release (Initial) Conditions. In NUREG/CR-5423, the two main scenarios were defined as releases of mainly metallic (Scenario II) or oxidic (Scenario I) melts, and the concern was that these may not be adequately enveloping. In particular, there was concern that this approach overlooked the possibility of a massive oxidic release, as in Scenario I, but with a very strong metallic component derived from melting of the upper internal (steel) structures. The problem was assigned to the Rensselaer Polytechnic Institute (RPI) for further investigation using the computer code APRIL.

2. Melt Spreading to the Liner. In NUREG/CR-5423, the possible range of melt depths against the liner was assessed under the assumption that for Scenario I the behavior is controlled primarily by hydrodynamic effects (which led to the Froude number scaled simulant experiments) and for Scenario II primarily by continuity and strong freezing behavior. The concern was that such quantification needed independent inputs with a more complete modelling approach. The problem was assigned to Argonne National Laboratory (ANL) for such evaluation using the computer code MELTSPREAD.

3. Corium Concrete Interactions. In NUREG/CR-5423, the corium superheat (temperature in

excess of the melt liquidus) was identified as one of the key parameters affecting the liner thermal loading, and it was determined that for the initial values in the range of interest to this problem, such superheats would decrease to zero rather rapidly. Also, it was concluded that the presence of zirconium and stainless steel and associated exothermic chemical reactions with the concrete decomposition products could not lead to chemical runaways. The assessments were made with the author's own analysis tool (COCA), and it was noted that an independent, audit-type evaluation using the state-of-the-art code CORCON-MOD 3 (not yet available at the time, but about to be released) would be quite appropriate. This task was assigned to the CORCON developer, Sandia National Laboratories (SNL).

4. Liner Failure Criteria. In NUREG/CR-5423, liner failure was assumed when any part of the liner reached 1500 °C, which is essentially a melt-failure criteria. The authors also carried out an elastic-plastic structural analysis (an ABAQUS finite element model of the whole spherical shell) and concluded that the stiffness of the cold portion of the liner was adequate to prevent catastrophic failure due to loss of strength along the narrow zone in contact with the melt. The remaining concern was whether the liner could be subject to failure at significantly lower temperatures by creep rupture.

Fortunately, at the time of the review, the needed high temperature creep data began to become available from a "lower head integrity" program at INEL, and the task was assigned to ANATECH, Inc., under subcontract to Sandia National Laboratories (SNL).

The work in these four areas took place in the mid-1991 to mid-1992 time frame, and as noted already, the results are documented in Parts II through V of this report. The purpose of this Part I is to incorporate these results in an updated quantification and to provide a synthesis of these by-parts quantifications according to the probabilistic framework. This is done in Chapters 2 and 3, respectively. Based on the understandings developed by the work, so far focused specifically to the Peach Bottom plant, we next take on the task of generalizing these results to all other Mark I plants. This is done in Chapter 4. From the liner attack standpoint, fuel-coolant interactions are beneficial, and they have been conservatively minimized in all these evaluations. However, fuel-coolant interactions can also produce mechanical loads, and quite as an aside to the main task here, this issue and its implications to containment integrity is addressed in Appendix A. We conclude there that steam explosions in a Mark-I geometry are of negligible structural impact. The final conclusions are given in Chapter 5.

2.0 Results of the Independent Evaluations and Discussion

2.1 Melt Initial Conditions (pdf1, pdf2, pdf3)

The APRIL modelling effort (see Part II) paid special attention to representing the upper internal structures (steam separator and dryer complex) and the thermal loading on them due to radiation from the upper portion of the core, and due to flow of highly superheated steam. It should also be noted that the APRIL.Mod3 code models include phenomena crucial to the melt relocation (core slump) and release (vessel failure); namely, corium solidification and crust/molten-corium-pool formation. This allows an "automatic" discrimination between Scenario I and Scenario II behaviors. This modelling capability was augmented by parametric variations in the set of 8 calculations carried out in this study (see Table 2.2 of Part II), including heat radiated to the upper structures, CRDHS flow rate, power distribution, and delays in the timing (above that calculated by the model) of RPV failure, to reasonably maximize release rates and ranges of composition in these releases during the "short term" defined in NUREG/CR-5423.

The bottom-line conclusion of this study was that "the bounding conditions used in the NUREG/CR-5423 report seem to be adequate." A direct perspective on this conclusion can be obtained from Table 1.3, in which the new results (from Tables 2.4 to 2.11 of Part II) are compared to what has been used in NUREG/CR-5423 (from Table 3.3 of the report) to envelop Scenario II behavior. As discussed in Part II, the upper bound value quoted in this table assumes that the entire mass of corium in the lower plenum is released into the containment at the time of vessel failure. Even so, the upper bound results for cases 1 through 4 show significant margins of conservatism in the NUREG/CR-5423 values. In cases 5 and 6, the "upper bound" values (i.e., ignoring quenching of the relocated melt in the lower plenum)

are physically inconsistent with the long residence time imposed by the arbitrary delay on RPV failure (20 and 60 minutes, respectively). Even so, the best estimate values are seen to be well enveloped by the NUREG/CR-5423 quantification. Finally, case 7, which was like case 2 but with an unrealistically uniform power distribution in the core, produced results well below the NUREG/CR-5423 values, except for the upper bound release rate which is slightly higher.

We note that both working group members (R.C. Schmidt and V.E. Denny)—see Appendix I—maintain certain reservations concerning the adequacy and completeness of this audit effort. The perspectives offered by these two reviewers as to the origin of respective concerns, however, is quite different. While Denny argues about the possibility of dissolving increased amounts of oxide in the metal phase within a gradual relocation frame as in Scenario II, Schmidt argues about increased melting of upper steel structures within a rapid/massive relocation frame as in Scenario I. Beyond the "final conclusion" of Part II, written so as to address these issues after they were raised by the reviewers, we would like to offer the following considerations.

On the potential of Scenario I with a large metallic fraction: We agree with Schmidt that at this time Scenario I cannot be excluded and it should, therefore, remain as a splinter scenario. We disagree, however, that a much increased (compared to what has been used in NUREG/CR-5423) metallic content within Scenario I is a legitimate possibility. This is because (a) the metals within the core melt out first and separate—they give rise to the crusts *assumed* for a Scenario I behavior, and (b) such a crucible configuration precludes gradual relocation to the lower plenum with the attendant steaming and convective heat transfer phenomena that give rise to the melting of the upper steel structures. In other

**Table 1.3. Comparison of APRIL.Mod3 Audit Results with the NUREG/CR-5423
Quantification of Melt Release Conditions**

NUREG/CR-5423		APRIL.Mod3, Case Number							
		1	2	3	4	5	6	7	8
Total Melt Volume (m ³)	14	6.5 [†]	7.7 [†]	9.7 [†]	9.3 [†]	12.7 [†]	16.0 [†]	13.2 [†]	**
"Short" Term Release		4.8*	6.4*	7.3*	8.5*	8.0*	8.0*	9.7*	
Melt Release Rate (m ³ /min)	0.7	0.65 [†]	0.77 [†]	0.65 [†]	0.62 [†]	1.27 [†]	1.6 [†]	0.90 [†]	**
		0.48*	0.64*	0.49*	0.56*	0.80*	0.8*	0.66*	

* Best estimate values taken as average between upper and lower boundary

[†] Upper bound value

**Results omitted as irrelevant – calculation assumed failure to depressurize the RPV

words, although such melting may be consistent with a Scenario II behavior, it is inconsistent with a Scenario I behavior. Both statements can be deduced, the first directly and the second indirectly, from the APRIL.Mod3 results found in this audit effort.

On the potential for Scenario II with a large oxide fraction: This is a somewhat mute point because (a) as indicated in NUREG/CR-5423 and substantiated by the MELTSPREAD calculations in Part IV, Scenario II releases are so slow as to be dominated by freezing phenomena on the drywell floor and thus do not pose a significant threat to the liner, and (b) the melt composition concern arises only because the lack of a dominant component would imply a high temperature melt (the oxidic component) *together* with a very highly superheated, highly conductive (compared to oxide) melt (the metallic component); this is not the case for solid entrained pieces, or eutectically dissolved oxides as postulated by Denny.

Based on the above we conclude that the initial conditions specified in NUREG/CR-5423 are conservative and appropriate for the final quantification.

2.2 Melt Spreading to Liner (CR1)

The MELTSPREAD modelling (see Part IV) includes all the wide-in-scope phenomena that participate in the spreading process, and this particular effort paid special attention to covering uncertainties by a wide range of parametric calculations. In addition, the calculations covered all potential combinations of melt volume, superheat, and composition from the distributions specified in NUREG/CR-5423. It is noteworthy that, among other verification aspects, the code compares well with the simulant tests reported in NUREG/CR-5423. The main results, reproduced from Figures 4.7, 4.8 and 4.9 of Part IV, are shown in Figures 1.5, 1.6, and 1.7, respectively. We see that the Scenario I quantification is found to be adequate while that for Scenario II is grossly conservative. In particular, for Scenario I we note that only the case of a 9 m³ release (three of the four parametrics on it) slightly exceed the upper bound—this is due to the relatively small volume that caused earlier immobilization, and it is inconsequential to liner integrity because of the small depths and superheats involved. For Scenario II, the behavior is dominated by freezing to such an extent that the melt does not even reach

Figure 1.5. The MELTSPREAD results for Scenario I shown in relation to the NUREG/CR-5423 quantification (CR1).

Figure 1.6. The MELTSPREAD results for Scenario II ("peak release") shown in relation to the NUREG/CR-5423 quantification (CR1)

Figure 1.7. The MELTSPREAD results for Scenario II ("average release") shown in relation to the NUREG/CR-5423 quantification (CR1).

the liner, except for the case based on an "average release" for a total volume of 30 m³. However, as indicated in Table 4.15 of Part IV, even in this case a large fraction of the melt against the liner is solid, the superheat of what melt remains is negligible, and the corium concrete interactions are so slow that augmentation of melt-to-liner heat transfer by bubble-stirring is also negligible.

As seen from Appendix I, there are no significant questions on these results by the three working group members (F. Moody, D.A. Powers, and M.Z. Podowski), and we can thus conclude that (a) the CR1 specified for Scenario I in NUREG/CR-5423 is appropriate and (b) the two CR1s specified for Scenario II in NUREG/CR-5423 are both unduly conservative. As explained in NUREG/CR-5423, this approach to Scenario II was made intentionally so, due to the lack of an analysis tool to create the basis for a more realistic position at the time. Now there is a sufficient basis to relax this conservatism and, in fact, in conjunction with the initial conditions

discussed in the previous section we can clearly see, with no further ado, that early liner failure in a Scenario II is physically unreasonable.

2.3 Duration of Melt Superheat (CR2)

The CORCON.Mod3 results (see Part III) confirmed that the duration of superheat scales with the melt depth (as proposed in NUREG/CR-5423) and found the original quantification of CR2 to be rather conservative. This can be seen by the direct comparison of the τ/H values (in the NUREG/CR-5423 nomenclature) computed for the 17 audit CORCON.Mod3 runs carried out for various permutations of initial superheat (ΔT_0), mass fraction of zirconium in the melt (ω), and melt depth (H), in the ranges of interest as indicated in Table 1.4 (extracted from Table 3.1 of Part III). Only Scenario I melts were considered. Note that the most extreme discrepancies appear for the extreme value (0.4) of the ω parameter, and the practically uninteresting extremes of very small depths (2.3 cm) and

superheats (10 K). In all other cases, separated out in bold in the table, the agreement is within about a factor of 2, much better in many cases, but always the NUREG/CR-5423 values are conservative.

The bottom line conclusion of this study is that "to a high level of confidence (>90%) the duration of superheat predicted with the CORCON.Mod3 code is less than what has been used" in NUREG/CR-5423, and as seen in Appendix I, both working group members (R.E. Henry and B.W. Spencer) agree with it.

In the present final quantification we will retain the original CR2, as found in NUREG/CR-5423, to represent a highly conservative bound. Recognizing the uncertainties in the rather complex, multicomponent chemistry and phase-behavior involved, this may not be too inappropriate. On the other hand, we also recognize the importance of the

results from the internationally acknowledged state-of-the-art analysis tool, CORCON.Mod3; we interpret them as "best estimate" and make use of them also in the synthesis of final results.

2.4 Liner Failure Criteria (CR3)

The ANATECH work (see Part V) fitted a viscoplastic model to the recently-obtained high-temperature creep data of INEL, and produced a three-dimensional finite element calculation (using ABAQUS) of the Mark-I shell subject to a thermal loading pattern similar to those found in NUREG/CR-5423. The bottom line conclusion of this work is that accounting for creep a "conservative best estimate" failure criterion is 1260 °C, and all three working group members (K. Bandyopadhyay, R.E. Henry and P. Shewman) appear to agree (see Appendix I). According to this result we must revise the failure criterion from the 1500 °C one

Table 1.4. Summary of the CORCON-MOD3 Results in Relation to the Quantification Employed in NUREG/CR-5423

#	ΔT_0 (K)	ω (-)	H (cm)	τ/H -CORCON (s/cm)	τ/H -COCA* (s/cm)
1	10	0.05	2.3	0.43	1
2	150	0.05	2.3	1.3	6
3	10	0.40	2.3	0.4	1
4	10	0.05	20.6	0.3	1
5	150	0.40	2.3	1.3	16.5
6	150	0.05	20.6	4.1	6
7	10	0.40	20.6	0.15	1
8	150	0.40	20.6	1.7	16.5
9	40	0.14	11.4	1.9	2
10	40	0.40	11.4	1.3	2.5
11	10	0.14	20.6	0.26	1
12	40	0.14	20.6	1.45	2.0
13	40	0.40	20.6	0.44	2.5
14	150	0.14	11.4	6.1	8.5
15	75	0.14	11.4	2.63	4.5
16	75	0.14	20.6	4.75	4.5
17	75	0.40	20.6	0.73	5.5

*This column is found from the CR2 in NUREG/CR-5423 as calculated from the COCA code.

Results and Discussion

used in NUREG/CR-5423 to 1260 °C in a "conservative" interpretation.

In the synthesis of final results we will combine this failure criterion with the highly conservative CR2 of NUREG/CR-5423, and an even more "bounding"

criterion of 1200 °C with the best estimate CR2 based on CORCON.Mod 3. Based on these failure criteria the CR3s were calculated in the manner of NUREG/CR-5423. The results are depicted in Figures 1.8 and 1.9.

Figure 1.8. The variation of critical superheat with superheat duration time and liner submergence for Scenario I melts, in the presence of water, for a liner failure criterion of 1260 °C.

Figure 1.9. The variation of critical superheat with superheat duration time and liner submergence for Scenario I melts, in the presence of water, for a liner failure criterion of 1200 °C.

3.0 Probabilistic Synthesis of the Sequence

As discussed in Section 2.2, only the Scenario I case needs to be explicitly calculated. We employ the simplified model presented in the body of NUREG/CR-5423, rather than that of Appendix I, as this is more appropriate to the liner failure results obtained in Part V (and Section 2.4). The two calculations carried out are described in Table 1.5. Note that in FCE-1 the quantification of each component is

conservative, with the possible exception of CR1, which may be considered as best estimate. In FCE-2, on the other hand, we exchange the CR3 quantification to an even more conservative bound for a CR2 quantification as a best estimate. The results are shown in Table 1.6, and in terms of the probability scale in Table 1.2, both clearly translate to a "physically unreasonable" event.

Table 1.5. Definition of the Two Enveloping Scenario I Calculations of Liner Failure Probability

Input Parameters	FCE-1*	FCE-2
pdf1, pdf2, pdf3 CR1 CR2 CR3	NUREG/CR-5423 NUREG/CR-5423 NUREG/CR-5423 1260 °C	NUREG/CR-5423 NUREG/CR-5423 CORCON.Mod3 1200 °C

*Final conservative estimate 1

Table 1.6. Final Conservative Estimates of Liner Probability*

FCE-1	FCE-2
3×10^{-3}	2×10^{-3}

*To be interpreted in terms of the scale in Table 2

4.0 Extrapolation to Other Mark I Plants

The original quantification in NUREG/CR-5423, and that discussed above, were for the Peach Bottom NPP. The purpose of this discussion is to provide some initial perspective of the potential extrapolation of these results to the other Mark-I plants. It is emphasized that we did not conduct a detailed plant-specific examination; rather, the discussion is based on data obtained from various sources, and again, it provides for an initial perspective and illustration of how one might go about such a task. This is why the discussion is provided in general rather than in plant-specific terms. In addition, it is noted that based on the results of NUREG/CR-5423 and the present work the NRC staff formulated a set of recommended guidelines which may serve as the basis of licensee's IPE submittals.

Examination of construction data of all Mark-I plants indicates that the pedestal doors are very similar, and that the principal floor dimensions, the drywell floor area, and the sump volumes (except for four cases for which this information could not be found) are equal to or larger than the Peach Bottom values.

Another important design parameter is the downcomer entrance height above the drywell floor. It appears that in two cases this parameter has been reported as zero. We do not know the design details, but both cases need to be examined as to whether they allow the possibility of melt running directly into the downcomer—such an occurrence would

almost certainly produce failure. In another case we found a reported height of 23 cm, which is a little less than one-half the Peach Bottom value. Even so, with reference to Figures 1.5 through 1.7, we conclude that this situation does not present a significant concern with respect to melt overflow into the downcomer(s) or having an inadequate degree of cooling of the liner above the melt (internal sprays). This absolute statement is further augmented when we recognize that this plant has about one-half the amount of fuel in the Peach Bottom plant. Then, there are four plants in which this height is ~30 cm; all four are of significantly lower power than the Peach Bottom plant, and for the reasons explained above, we see no particular concerns. In all other plants the height is significantly larger than 40 cm, and again, the Peach Bottom results are applicable.

As a separate issue, we made an attempt to look at the possibility that floor-mounted equipment in the drywell, especially across from the pedestal door, could present any significant obstacles with a significant impact on the melt spreading process. We could learn that this was not the case, but we did not have the materials necessary to examine the relevant construction details (for any containment) directly for this purpose. Clearly, this is a subject well-suited to the individual plant examinations, and the guidance mentioned above specifically provides for it.

5.0 Concluding Remarks

At this time we believe we have achieved a very substantive synergism and convergence among multiple independent evaluation efforts and multiple review processes involving a total of thirty experts. The final quantification, as provided in this report, confirms the NUREG/CR-5423 conclusion that in the presence of water, early liner failure is "physically unreasonable."

The ROAAM process was initiated in January 1990 by the issuance of Draft NUREG/CR-5423, and it is expected to be completed in May 1993, following the completion of the peer review of this report. It took a total of 3-1/2 years, which were devoted roughly equally to the initial review and issuance of NUREG/CR-5423 (August 1991), and to the four independent audit studies and their respective peer

reviews. This time element is a crucial aspect of ROAAM, and now that the process has been demonstrated for the first time, it is important to emphasize this time element again. Also, we wish to point out that besides the practical aspects of carrying out what needs to be done, the relaxed time element provides certain intangible but important benefits by allowing "digestion" time for all those involved in the process as well as external inputs (through the literature) by any who are not involved. To be explicitly stated, these are essential requirements for stability in the convergence being aimed for, and for additional safeguards against "outliers." Of course, in the final analysis, the timing depends on the scope and complexity of the problem, and with four major elements involved, the Mark I problem can be recognized as one of the most demanding ones.

APPENDIX A

**THE OCCURRENCE AND ROLE OF STEAM EXPLOSIONS
IN A MARK-I CONTAINMENT**

Appendix A

The Occurrence and Role of Steam Explosions in a Mark-I Containment

In the main body of this report we concluded that water in the drywell can prevent failure of the containment shell by thermal attack. On the other hand, it is known that molten, high temperature metallic or oxidic materials in contact with water can explode; the question arises then as to whether such explosions can lead to containment (liner) failure themselves. This question is addressed in this appendix. The intent is to offer a scoping evaluation, which, clearly, is not part of the disciplined approach of ROAAM, described in the body of this report for the thermal attack problem.

To begin with, several points of clarification must be made, as follows:

1. Besides the direct failure, by impulse loading, of the liner, failure can also occur by pedestal failure and toppling of the reactor vessel. As we explain below, this is in fact the dominant failure mode to be considered.
2. Energetic explosions with the oxidic component of reactor materials (UO_2) have never been observed. However, highly energetic explosions were found experimentally with molten alumina alone (as in the KROTOS experiments) or in mixture with iron, as in an iron-alumina thermite often used as a corium simulant (as in the RC experiments in Sandia, or in the ALPHA experiments in Japan). It is generally expected that corium (or UO_2) can lead to energetic interactions.
3. Explosive interactions have been found in stratified geometries (Bang and Corradini, 1991; Frost et al., 1993; Sainson et al., 1993); however, never in the presence of intense corium-concrete interactions, i.e., in the real-material experiments in the MACE program, or even with the relatively more explosive metallic melts in the SWISS tests. Also, the above-referenced experiments show that stratified explosions are energetically benign, involving a coupling length scale (i.e., depth of "melt" participating in the explosion) of less than ~ 1 cm.

Turning next to the particular melt release conditions of interest here, and resulting mode of contact, we can identify several explosion scenarios (ES) as follows (see Figures 1.1 and 1.2):

- a. Pouring Contact Mode (i.e., ES-PCM). This could arise as the released melt enters the water pool just below the reactor vessel.
- b. Stratified Contact Mode (i.e., ES-SCM). This arises during or after spreading (but prior to the formation of a solid crust on top) and includes both the in- and the ex-pedestal regions.
- c. Advancing Contact Mode (i.e., ES-ACM). This involves the leading edge of the melt as it spreads on the concrete. In the early stages of the release it is found inside the pedestal, later on it evolves in the annular region (ex-pedestal).

We will argue that the ES-ACM can produce only local, small-scale explosions relevant more to quenching than to energetics; that the ES-SCM can be ruled out under the conditions relevant to the Mark-I conditions of interest here; and that the ES-PCM are of negligible impact on the structural integrity of pedestal (there is no significant coupling mechanism to the liner).

The ES-ACM (to our knowledge not discussed previously) can be visualized as a two-step process. Because of water trapped between the melt and the concrete, concrete wetness, and the experimentally-observed very rapid initial (upon contact) decomposition of the concrete (for example, see Gronager et al., 1986), we can visualize that the leading edge would actually undergo some sort of short-range periodic dispersals leading to respective premixtures. The conditions are also favorable for generation of pressure pulses, and thus of triggering of explosions in such premixtures. It is important to note that these are *very* small scale phenomena, much more relevant to quenching than energetics. At the extreme, such a process could prevent the formation of stratified geometry—i.e., eliminating liner thermal attack altogether. In a more moderate behavior these small, transient premixtures would settle back into a stratified configuration with the melt flowing as a spreading layer on the concrete and the water on top; that is, a configuration for which the ES-SCM needs to be addressed.

In addressing the likelihood of an ES-SCM, the fundamental consideration is on the flow regime (or phase configuration) at the melt-water interface. In all the stratified explosion experiments mentioned above, this interface was characterized by relatively quiet film boiling conditions in which the melt and water were separated by a very thin vapor blanket and sporadic vapor bubbles departing from it. A trigger, in the form of a pressure pulse, can easily propagate along such an interface because even a slight fluid displacement is sufficient to collapse the vapor blanket, and this can occur even under a rather small inertia constraint (i.e., small depth of the coolant layer on the top of the melt). By contrast, a fully molten corium layer (as a necessary condition for an explosion the melt at the interface must be fully molten) on top of concrete will produce intense concrete decomposition (e.g., Gronager et

al., 1986), and gas generation at rates typically of ~100 cm/s (limestone common sand concrete), which together with the very high steaming rates (very high radiation heat fluxes) would yield a highly agitated and highly dispersed interface. Such an interface is not susceptible to propagation, especially under the very small inertia constraints possible in a Mark-I geometry. We conclude, therefore, that the ES-SCM can be ruled out.

Finally, we turn to the ES-PCM. Such an explosion scenario can in fact occur, especially in the very early stages of the pour. The site of the explosion should be somewhere near the middle of the in-pedestal area, and we note the absence of mechanisms for missile generation. Thus, the water pool (30 cm maximum depth) provides the only medium for coupling the explosion energy release to the pedestal wall. This coupling occurs by the propagation of a pressure pulse through this shallow pool of water to the wall, and the damage potential can be judged by the resulting impulse on the wall. From an investigation of the load-bearing capability of such pedestal structures done in NUREG-1150, significant damage is expected in the range 30 to 150 kPa(s). Recognizing that only a very small fraction of the wall is loaded (and very near the foundation) in our case, let us take the upper limit of 150 kPa(s). For a 2 ms pressure pulse, this translates to a peak pressure of 1000 bar, and at least an order of magnitude higher pressure at the source (3 meters away at the center of the pedestal). Such explosion pressures are impossible to generate, even for arbitrarily conservative premixtures in a pool depth of 30 cm because of explosion venting. In addition, propagation of such pulses are prohibitive because again of "venting" of the pressure pulse as it is propagated along the shallow pool for such large, relative to depth, distances (large aspect ratio). Both of these "venting" processes have been

introduced and discussed by Yuen and Theofanous (1993), and they are illustrated in Figure A.1. Thus,

we conclude that the ES-PCM is also of negligible structural impact.

PRESSURE AT TIME = 1.5 MS

PRESSURE AT TIME = 3.0 MS

PRESSURE AT TIME = 4.5 MS

PRESSURE AT TIME = 6.0 MS

Figure A.1. Illustration of the "venting" of a steam explosion in a shallow water pool. The pool is 1 m deep, its bottom is located at $z = 0$, and it is 6 m in diameter. The premixture is found within a cylinder 40 cm in diameter around the pool axis which is located at $r = 1$. The explosion was triggered at the center bottom of the pool ($1 < r < 2, 1 < z < 2$). For details see Yuen and Theofanous (1993).

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PART II:
THE EFFECT OF HEATUP AND MELTDOWN OF REACTOR VESSEL
UPPER INTERNALS ON THE COMPOSITION OF CORIUM
DISCHARGED INTO THE DRYWELL OF BWR MARK-I CONTAINMENT

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Executive Summary

The objective of this project was to analyze the effect of heat transfer from a degraded and partially molten BWR core on the heatup and possible melting of the reactor pressure vessel (RPV) upper structures, and the resultant impact on the composition of corium released into a MARK-I containment after the RPV failure. A particular question under investigation was concerned with the adequacy of the bounding calculations used in the NUREG/CR-5423 Report.

The present analysis was done with the APRIL.MOD3 code, in which the heatup, collapse and melting of the BWR steam separator/dryer complex are explicitly modeled. The basic findings can be summarized as follows:

Whereas the combined heat radiation/convection to the upper structures may have a significant effect on the amount of melt released into the containment, the APRIL.MOD3 predicted major characteristics of the melt during the phase of high release rates stay within the range of values used by Theofanous et al. [1990]. Therefore, it can be concluded that, within the scope of this work, the bounding conditions used in the NUREG/CR-5423 report seem to be adequate.

1.0 Introduction

Although a considerable progress has been recently made in the understanding of the mechanisms governing severe accidents in nuclear power plants, any predictions of the propagation and consequences of hypothetical accidents using existing methodologies and computer codes are still associated with substantial uncertainties. This is mainly due to the profound complexity of core heatup and meltdown phenomena, and their impact on the integrity of the reactor vessel and containment.

One of the important issues in the analysis of accident consequences in BWRs and with MARK-I containments is concerned with the possibility of drywell liner failure as a result of attack by hot corium spreading over the drywell floor. The probability of failure strongly depends

on the composition of corium released from the reactor pressure vessel [Theofanous et al., 1990].

The objective of the present work was to perform numerical analyses in which the effect of heat transfer from a degraded and partially molten reactor core towards the upper internals (upper shroud, standpipes, steam separator and dryer) in a BWR reactor pressure vessel (RPV) would be accounted for, and the resultant failure and melting of these structures, on the composition of corium released to the drywell following RPV failure. The calculations for this study were performed using the APRIL.MOD3 computer code, and the Peach Bottom BWR was used a reference power plant with MARK-I containment.

2.0 Project Description

The propagation and consequences of severe reactor accidents are governed by several factors of a different nature, including both deterministic and stochastic processes. The state-of-the-art in the modeling of accident phenomena varies considerably with the stage of accident. The initial phase of core heatup has been investigated quite extensively before, including both analyses and experiments. As a result, numerical predictions for this phase (in particular, prior to core degradation) agree quite well with the existing experimental data. However, when an advanced phase of core heatup and meltdown is reached, deterministic models become much less reliable due to the increasing randomness of potential failure mechanisms of the reactor system pressure boundaries and of the resultant corium spreading. In particular, the above applies to estimating the composition of corium attacking the drywell liner in MARK-I containments.

In the analysis of MARK-I liner failure performed by Theofanous et al. [1990], two basic corium release scenarios have been examined. In one of them a predominantly oxidic corium was assumed, and corium containing mainly metallic phase was considered in the other. The results shown in the above-mentioned report indicate that in either case the probability of liner failure is very low. A common factor in both cases was the assumed low superheat of molten corium. The reasoning behind using $T_{\text{sup}} = 700^{\circ}\text{C}$ as the upper limit for melt superheat was that (especially in the case of high-temperature oxidic corium) the heat transfer between the corium and the reactor vessel would result in the lower head failure, and the subsequent melt release, thus preventing any substantial superheating of the molten corium.

A considerably different situation might occur if the composition of corium at the time of RPV failure was intermediate between the values used in the two scenarios (Scenario-I and Scenario-II) examined by Theofanous et al. [1990]. Namely, if the corium is composed of similar fractions of both oxidic and metallic phases, the latter constituent (in particular, steel) may reach a significantly higher superheat than that used in NUREG-5423, even if the average corium temperature remains unchanged. The question is if there exist hypothetical BWR accident sequences which may lead to the in-vessel formation of a substantial amount of mixed oxidic-metallic corium which could be released onto the drywell floor after RPV failure.

Possible severe accident scenarios in BWRs, which bear the potential of producing a large amount of molten steel mixed with molten (partially at least) UO_2 , are those resulting in the failure and melting of the upper vessel internals. The specific events leading to the release of significant quantities of steel, are still unclear.

The purpose of the present work was to investigate accident sequences in a typical BWR with MARK-I containment which may lead to the heatup, failure, and subsequent melting, of the steam separator/dryer complex inside the reactor pressure vessel. The analysis was performed using the APRIL.MOD3 severe accident computer code. In addition, scoping calculations were performed to assess the effect of heat radiation to the upper structures on the heatup and melting of these structures, and to verify the APRIL results.

3.0 Overview of the APRIL.MOD3 Code

APRIL is a computer code developed at Rensselaer Polytechnic Institute under the sponsorship of Oak Ridge National Laboratory (originally, as a subcontract of the US NRC project) and Empire State Electric Energy Research Corporation (ESEERCO). Some of the features of APRIL.MOD3 which make this code a useful tool for the present analysis, are:

- APRIL has been developed especially for BWRs, and contains many mechanistic modes of phenomena not accounted for in other codes, such as: gradual relocation of molten materials and their refreezing in contact with cold structures, channel blockage and crust remelting, and heatup and possible failure of the steam separator/dryer complex.
- Several models in APRIL have been validated against detailed experimental data, including those taken at Rensselaer and elsewhere. In particular, recently completed comparisons against the SANDIA DF-4 and KfK CORA-16/17 experiments have shown excellent agreement between the APRIL calculations and experimental results.
- APRIL.MOD3 has a built-in restarting option, which allows for modifying input parameters during the calculations. This is very useful when parametric analyses are to be performed of various user-specified parameters and different accident conditions (such as the status of selected safety systems).

The APRIL code, and APRIL.MOD3 in particular, has been extensively documented, including reports on model description and testing [Koh et al., 1985; Kim et al., 1988; Lahey and Podowski, 1990], an APRIL.MOD3 user manual [Cho et al., 1991], and the reports showing the results of model validation against experimental data.

The results of APRIL.MOD3 code comparison exercises so far confirm the validity of the modeling assumptions used and consistency of the overall modeling concept. In comparison with other severe accident analysis codes, APRIL.MOD3 yields results which can be judged, according to the existing state of the art, as realistic but containing some degree of conservatism. In particular, APRIL.MOD3 was compared against the results [Abramson and Komoriya, 1988] of other severe accident codes such as: STCP, MARCH/RMA, and MAAP 3.0. Details concerning the results of this comparison can be found in [Podowski et al., 1989] and [Lahey and Podowski, 1990].

The modeling concepts in APRIL.MOD3 which are important for the present work include, in particular, the following:

- (1) Melting of various core materials and melt relocation inside the core, and from the core onto the lower core plate, and into the lower plenum,
- (2) Heatup, failure, and melting of the upper vessel internals, and the relocation of molten steel through a degraded core.

The above-given models are summarized below. Additional information can be found in [Koh et al., 1985], [Kim et al., 1988], and [Lahey and Podowski, 1990].

The core model in APRIL.MOD3 uses a two-dimensional (in axial and radial directions) nodalization scheme. In each node the following core structures are accounted for: fuel (UO₂ pellets), cladding, canisters, and control rods. During the initial phase of core heatup and oxidation, the nodal temperatures of each structure are calculated separately. When either the temperature of control rods, cladding, or canister

Overview

walls, reaches the melting temperature of the respective material (stainless steel or zircaloy), the time-dependent mass replaces temperature as a state variable. The formation, and propagation, of rubble bed inside the core may result in a gradual, radially-dependent, reduction in the core height.

The specific in-core phenomena modeled in the APRIL code include: 2-D heat transfer between individual core components as well as between the core and surrounding structures, heat convection to the coolant in both the covered and uncovered portions of the core, oxidation of the fuel cladding, channel walls, and control blades, fuel buckling (used as a criterion for rubble bed formation), slumping of the canisters and control blades, melting of individual core components, the formation of a eutectic mixture of fuel and molten zircaloy, molten material relocation and refreezing, channel blockage and the resultant flow redistribution.

The core model is coupled with the model of in-vessel thermal-hydraulics. Various flow configurations inside the vessel are accounted for, including the formation of a natural circulation loop between the hot (fully uncovered) core and the relatively cold downcomer regions. The in-vessel model provides the core model with boundary conditions such as the gas/steam mixture inflow temperature, system pressure, and the temperature of surrounding structures.

A model of particular importance for the present analysis is concerned with the heatup and possible melting of the upper in-vessel structures (upper shroud head, standpipes, steam separators, and steam dryers). The shroud-head/steam-separator assembly consists of a domed base on top of which is welded an array of standpipes with a multi-stage steam separator located on the top of

each standpipe. The entire assembly, made of stainless steel, rests on the top guide grid and forms a cover for the core outlet plenum region. The steam dryer assembly is mounted in the reactor vessel above the shroud-head/steam-separator assembly. Since, in the case of an accident, the upper shroud head may be directly exposed to a high-temperature core, the combined effects of radiation from the core and convective/radiative heat transfer from the hot steam/gas mixture in the upper plenum, may increase the shroud temperature to failure point. When the weakened shroud head cannot support the mass above it, the upper structures may collapse onto the core (except for the steam dryer which has a separate support system). The molten steel from these structures may penetrate the hot and partially molten core and flow into the lower plenum and, following lower head failure, into the containment.

In the APRIL.MOD3 code, a lumped-parameter energy equation is used to calculate the average temperature of each structure separately. These equations account for heat transfer modes such as: heat radiation from the top of the core, combined heat convection/radiation from/to the steam/gas mixture (both on the core and downcomer sides), Heat conduction between the neighboring structures, and direct heating due to the deposition of fission products. The failure of the shroud head is calculated by comparing the actual weight-induced stress against the temperature-dependent ultimate tensile strength of stainless steel. After the failure, the upper shroud head, together with the standpipes and steam separators, is assumed to relocate downwards and rest on the top of the core. When the temperature of any given structure reaches the melting point of stainless steel, the respective energy balance is used to calculate the melting rate of the structure's material.

4.0 Scope of Work

As was mentioned in Section 2.0, the specific purpose of this work was to use the APRIL.MOD3 code to determine if a significant amount of metals might be mixed with the liquid oxides formed in the core during a BWR severe accident. To this end, APRIL.MOD3 has been employed to analyze the consequences of a hypothetical short-term station blackout accident in one unit of the Peach Bottom power plant. Two major tasks in the course of the work were as follows:

Task-1 : The development of a complete model of the Peach Bottom reactor and containment systems, and its implementation as an input file in the APRIL.MOD3 code.

Task-2 : The application of APRIL.MOD3 to investigate the propagation of core and RPV heatup and melting, and their impact on the composition of corium released to the MARK-I drywell of Peach Bottom nuclear power plant.

In Task-1, a set of geometrical parameters and basic operational characteristics of the Peach Bottom BWR with MARK-I containment was examined and applied to develop a complete input file for the APRIL.MOD3 code. The resultant model includes the reactor, the reactor pressure vessel (RPV), and the containment. The necessary plant data was provided by the project sponsor.

In Task-2, the effect was studied of heat transfer from the core to the upper structures on the heatup and melting of these structures. The input parameters concerning the accident sequence of reference were identified based on the conditions used before at ORNL for the BWRSAR calculations of a short-term station blackout at Peach Bottom. These conditions were set up according to the emergency procedures

guidelines (EPG) for the Peach Bottom BWRs. In addition, parametric calculations were performed using APRIL.MOD3 in which the effect of selected input parameters on the progression of melting was quantified. The various accident simulations included calculations in which the following parameters were changed:

- (a) radiative heat transfer between the degraded core and the upper structures (steam separator/dryer complex) in the reactor pressure vessel (RPV),
- (b) CRD hydraulic system (CRDHS) flow rate,
- (c) timing of RPV failure,
- (d) axial power distribution.

The effect of radiative heat transfer was tested by using a multiplication factor in the expression in APRIL.MOD3 calculating the radiative power exchanged between the top of the core and the upper structures. Specifically, the base-case calculations were compared against the results in which the radiation heat transfer from the core was reduced to 10 percent its original value. The purpose of this study was to quantify the uncertainties associated with the emissivities of the various materials in the reactor vessel, modeling simplifications in regard to the view factors for radiation, etc. The rationale behind considering only cases of reduced radiative power was that the base-case calculations already indicated that radiation may play a significant role in the heatup and melting of the upper structures.

It was assumed in the calculations that the only core cooling (if any) was via the CRDHS flow. In particular, the no-flow situation was considered so that the possibility could be examined of the upper structure heatup via radiation only. In addition, calculations were performed including a 60 g/min CRDHS flow in order to test the impact

Scope

of two competing effects on the progression of the in-vessel heatup and melting: (a) the role of CRDHS flow as heat sink and, since the added small amount of water is insufficient to cool the reactor, the effect of enhanced-evaporation-induced oxidation.

The amount and composition of melt released into the containment depend on the mode of reactor pressure vessel failure. Because of the complexity of the phenomena governing melt relocation and the resultant lower head failure, substantial uncertainties are inherently present in any analytical/numerical predictions of the timing of RPV failure. In the present calculations the APRIL.MOD3 code predicts a local failure of the lower head due to the drain plug meltdown when, following the lower core plate failure, molten fuel is released into the lower plenum. In order to test the sensitivity of calculated melt release into the containment to the timing of RPV failure, two accident scenarios were considered in addition to the original APRIL.MOD3 predicted sequence. In one of them, the RPV failure was delayed by approximately 20 min, and by about one hour in the other, both relative to the basic case.

In order to parametrically test the effect of different pre-accident conditions, in particular the reactor operation time prior to the accident, calculations were performed for two different axial power distributions in the core. Namely, in addition to the power distribution obtained from the

project sponsor, in which a typical end-of-life (highly nonuniform, bottom-peaked) power distribution was used, the APRIL.MOD3 code was also run for a situation simulating the other end of possible axial power profiles, in which a uniform axial decay heat generation rate was assumed.

In addition to changing the parameters described above, additional calculations were also performed to quantify the effect of ADS on the progression of reactor meltdown. Since high-pressure accident sequences are not the subject of the present investigations (only low pressure melt ejections were considered in the MARK-I liner attack analysis by Theofanous et al. [1991]), the main purpose of the APRIL.MOD3 calculations for such situations was to confirm that this code properly captures the effect of in-vessel conditions prior to the failure on the parameters of melt released to the containment after the failure.

Since the primary objective of the present study was to check the impact of upper structures melting on the amount and composition of melt released into the containment, it was deemed important to obtain an independent confirmation concerning the correctness of the results of APRIL.MOD3 calculations. For this purpose, calculations using a stand-alone model were performed in order to estimate the amount of heat radiated from the top of the degraded BWR core to the upper vessel internals and the resultant heatup and melting rate of these structures.

5.0 Description of Situation Analyzed

As mentioned in Section 4.0, the accident sequence which was used as reference in the present study was a short-term station blackout at Peach Bottom. The input to APRIL.MOD3 was developed based on the data provided by the project sponsor. These data include the plant geometry and pre-accident operating conditions as well as some parameters concerning the accident sequence. The latter included, in particular, the decay heat power curve used before in BWRSAR calculations (see Figure 2.1) and the prescribed timing of selected events, as shown in Table 2.1.

The above-mentioned data were used to develop an input file for the APRIL.MOD3 code, which will be referred to as the base case (Case 1 in Table 2.2). Later, selected modifications in the input parameters were introduced in order to investigate the various parametric effects discussed in Section 4.0. The specific runs the results of which are presented in this report are also summarized in Table 2.2.

Figure 2.1. Decay heat power used in the APRIL.MOD3 simulation of a short-term station blackout accident at Peach Bottom

Table 2.1. Main events in short-term station blackout accident scenario at Peach Bottom, used as input in the APRIL.MOD3 simulations of core meltdown progression

Event	Time from the beginning of calculation (min)
1. Reactor scram after loss of offsite power	-35
2. Beginning of core uncovering (swollen level falls below top of core)	0
3. ADS system actuation	45

Table 2.2. Cases studied in the parametric analysis of corium composition during short-term station blackout at Peach Bottom

Case	Heat radiation to RPV upper structures	CRDHS flow rate [gal/min]	ADS	Power distribution	Time of RPV failure
1	100%	0	yes	ref.	$T_{calc}^{(*)}$
2	10%	0	yes	"-	"-
3	100%	60	yes	"-	"-
4	10%	60	yes	"-	"-
5	10%	0	yes	"-	$T_{calc}+20min$
6	10%	0	yes	"-	$T_{calc}+60min$
7	10%	0	yes	uniform	T_{calc}
8	10%	0	no	ref.	T_{calc}

(*) T_{calc} is the RPV failure time predicted by the APRIL.MOD3 code.

6.0 Stand-Alone Model of Heat Radiation Between Degraded Core and RPV Upper Internals

In order to obtain an independent verification of the APRIL.MOD3 model of heat transfer between the core and the upper vessel internals, a stand-alone model has been developed to calculate the heatup (and melting) rate of the upper structures exposed to the combined effects of heat radiation from the top of the degraded core and from the gas (steam/hydrogen mixture) heated when passing through the uncovered and partially molten core. Among others, this model was used to check the sensitivity of the calculated rate of heat transfer to the temperature of the top of the core and to the assumed emissivities of solid surfaces and the gas.

In the model, a three body enclosure is considered, including two solid surfaces exchanging thermal radiation and a participating gas filling the space between these surfaces. As shown in Figure 2.2, the lower surface represents the top of the degraded core and the upper surface represents the shroud head.

Using the network shown in Figure 2.3, the radiative heat transfer between the elements of the enclosure shown in Figure 2.2 can be modeled by the following equations:

$$\frac{E_{b,c} - J_c}{R_c} + \frac{J_s - J_c}{R_{c-s}} + \frac{E_g - J_c}{R_g} = 0 \quad (1)$$

$$\frac{E_s - J_s}{R_s} + \frac{J_c - J_s}{R_{c-s}} + \frac{E_g - J_s}{R_g} = 0 \quad (2)$$

$$\frac{E_g - J_s}{R_g} + \frac{E_g - J_s}{R_g} = 0 \quad (3)$$

where $E_b = \sigma T^4$ is the black body emissive power of a surface at a temperature, T , J is the radiosity, R is the resistance to radiation, and the subscripts c , s , and g refer to core, shroud head and gas, respectively.

It has been assumed in Equations (1) through (3) that the view factor from the core to the shroud head is equal to unity (which is true if the top of the core is treated as a flat surface). The same values have been used for the view factors between the gas and each of the solid surfaces.

The resistances of the solid surfaces can be expressed as

$$R_c = (1 - \epsilon_c) / \epsilon_c \quad (4)$$

$$R_s = (1 - \epsilon_s) / \epsilon_s \quad (5)$$

the gas resistance is given by (assuming that the gas is nonreflective)

$$R_g = 1 - \epsilon_g \quad (6)$$

and the resistance for heat radiation directly between the code top and the shroud head is

$$R_{c-s} = 1 / (1 - \epsilon_g) \quad (7)$$

where ϵ_c , ϵ_s , and ϵ_g are the emissivities of the respective materials.

Solving Equations (1) through (3) for J_c , J_s , and E_g , yields

$$J_c - J_s = (E_c - E_s) / \{1 + [(2R_g)^{-1} + (R_{c-s})^{-1}](R_c - R_s)\} \quad (8)$$

$$E_g - J_s = (E_g - J_c) = (J_c - J_s) / 2 \quad (9)$$

Equations (8) and (9) can be used to evaluate the heat flux received by the shroud head,

$$q_s'' = (E_c - E_s) / R_{eff} \quad (10)$$

where

$$R_{eff} = [(R_{c-s})^{-1} + (2R_g)^{-1}]^{-1} + R_c + R_s \quad (11)$$

For a given area of the shroud head, A_s , the total heat rate received by the shroud head is

$$q_s = q_s'' A_s \quad (12)$$

Equation (12) has been used to parametrically estimate the thermal power received by the shroud head for various temperatures of the top of the core and various emissivities of the solid surfaces and the gas. The results are discussed in the next section.

Figure 2.2. A schematic of the geometry used in the stand-alone model of radiative heating of the upper in-vessel structures

Figure 2.3. Radiation network for the model in Figure 2.2

7.0 Results of Calculations

The results of APRIL.MOD3 calculations for all the cases listed in Table 2.2 are shown in Appendices A through H. The timing of the main events calculated for Case 1 is shown in Table 2.3.

The APRIL.MOD3 results were also used to evaluate the amount of and composition of corium released into the containment and to show the effect of heat transfer to the upper structures. The basic parameters characterizing melting of the upper structures and corium delivery to the containment are shown in Figure 2.4 and Tables 2.4 through 2.11.

The corium release results shown in Tables 2.4 through 2.11 are based on averaging the values obtained for two bounding cases. The upper bound case assumes that the entire mass of corium in the lower plenum is released into the containment at the time of vessel failure. On the other hand, the lower bound case assumes that corium (metallic phase) released to the lower plenum prior to the vessel failure freezes in contact with the water in the plenum, so that only the metals melted after the failure can be released into the containment. Since any specific predictions of the amount of melt at the time of vessel failure would be very uncertain, averaging between these two most extreme situations was deemed to be the best approach to obtain realistic estimates of the corium release into the containment. Nevertheless, the results of the two bounding calculations are also shown in Tables 2.4 through 2.11.

As can be seen in Figure 2.4 (also, see Figure A-3), the APRIL.MOD3 calculated melting rates of the shroud head and steam dryer are about 7.4 kg/s and 8.4 kg/s, respectively, whereas the average melting rate of the standpipes and steam

separator is about 28.5 kg/s and for a short time increases to a 100 kg/s range. Both the higher average value of the standpipes and steam separator and, in particular, the narrow peak observed at approximately 195 min, are due to the enhanced heat convection following melt discharge into the water pool in the lower plenum. The evaporation of water increases the steam flow rate and, therefore, the convective heat transfer coefficient to the upper structures, whereas the simultaneous oxidation of the remaining zircaloy in the core increases the steam/gas mixture flowing from the degraded core towards the upper regions of the reactor vessel. It is important to notice that (see Table 2.3) this accelerated melting occurs after the failure of the reactor pressure vessel.

In order to verify the APRIL.MOD3 predictions concerning the heatup and melting of the in-vessel upper structures, the model discussed in Section 6.0 was used to obtain parametric estimates of the heat rate received by these structures and of their melting rates. Specifically, the effective resistance for heat radiation between the core upper surface and the upper structures (see Equation 11) was calculated for various assumed values of the emissivities of the solid surfaces and the gas. The results of these calculations are shown in Table 2.12.

Table 2.12 covers a broad range of emissivities. Whereas the actual emissivity of stainless steel (at room temperature) varies from 0.16 (type 301 polished) to 0.39 (type 347, cleaned), after repeating heating and cooling the emissivity of various types of steel may substantially increase (to 0.57 at 230°C) [Ozisik, 1985]. Hence, it is not unrealistic to anticipate the emissivity of the top surface of the degraded reactor core to be about 0.5.

Results

**Table 2.3. APRIL.MOD3-calculated main events in a short-term station blackout accident at Peach Bottom.
Case 1.**

Event	Time from the beginning of calculation [min]
Reactor scram after loss of offsite power	-35
Beginning of core uncovering (swollen level falls below top of core)	0
ADS system actuation	45
Melting of zircaloy begins	114
Melting of control blades begins	117
Relocation begins of molten steel onto lower core plate	129
Beginning of zircaloy relocation onto lower core plate	153
Lower core plate fails	154
Shroud head reaches melting point	155
Beginning of UO ₂ relocation into lower head	169
Reactor vessel fails	173
Fast melting of upper structures begins	182

Figure 2.4. Melting rate of the upper vessel structures for the basic case (Case 1)

Results

Table 2.4. Summary of APRIL.MOD3 results for Case 1

Time of RPV failure		173 min		
Melting of RPV upper structures				
Melting of RPV upper structures	Duration of high melting period, t_m [min]	36		
	Average melting rate during t_m [kg/s]	28.5		
	Total molten mass during t_m [kg]	62,000		
Corium release to the containment				
Time interval	0 to 10 min after RPV failure	During 10 min interval at highest release rate of oxidic phase		
Total mass released to the containment [kg]	35,000 (45,000 ^a , 21,000 ^b)	12,000		
Total volume released to the containment [m ³]	4,8 (6.5 ^a , 3.1 ^b)	1.6		
Rate of release [m ³ /min]	0.48 (0.65 ^a , 0.31 ^b)	0.16		
Composition of corium released onto drywell floor [% mass]	metal	oxide	metal	oxide
	94 (92 ^a , 96 ^b)	6 (8 ^a , 4 ^b)	75	25
(a) and (b), respectively, refer to the upper and lower bounds of mass released.				

Table 2.5. Summary of APRIL.MOD3 results for Case 2

Time of RPV failure		174 min		
Melting of RPV upper structures				
Melting of RPV upper structures	Duration of high melting period, t_m [min]	30		
	Average melting rate during t_m [kg/s]	36		
	Total molten mass during t_m [kg]	65,000		
Corium release to the containment				
Time interval	0 to 10 min after RPV failure	During 10 min interval at highest release rate of oxidic phase		
Total mass released to the containment [kg]	44,000 (53,000 ^a , 34,000 ^b)	22,000		
Total volume released to the containment [m ³]	6.4 (7.7 ^a , 5.0 ^b)	2.9		
Rate of release [m ³ /min]	0.64 (0.77 ^a , 0.50 ^b)	0.29		
Composition of corium released onto drywell floor [% mass]	metal	oxide	metal	oxide
	97 (98 ^a , 96 ^b)	3 (2 ^a , 3 ^b)	82	18

(a) and (b), respectively, refer to the upper and lower bounds of mass released.

Results

Table 2.6. Summary of APRIL.MOD3 results for Case 3

Time of RPV failure		172 min		
Melting of RPV upper structures				
Melting of RPV upper structures	Duration of high melting period, t_m [min]	40		
	Average melting rate during t_m [kg/s]	21		
	Total molten mass during t_m [kg]	50,000		
Corium release to the containment				
Time interval	0 to 10 min after RPV failure	During 10 min interval at highest release rate of oxidic phase		
Total mass released to the containment [kg]	53,000 (70,000 ^a , 35,000 ^b)	12,000		
Total volume released to the containment [m ³]	7.3 (9.7 ^a , 4.9 ^b)	1.4		
Rate of release [m ³ /min]	0.49 (0.65 ^a , 0.33 ^b)	0.14		
Composition of corium released onto drywell floor [% mass]	metal	oxide	metal	oxide
	87 (91 ^a , 83 ^b)	13 (9 ^a , 17 ^b)	55	45
(a) and (b), respectively, refer to the upper and lower bounds of mass released.				

Table 2.7. Summary of APRIL.MOD3 results for Case 4

Time of RPV failure		173 min		
Melting of RPV upper structures				
Melting of RPV upper structures	Duration of high melting period, t _m [min]	40		
	Average melting rate during t _m [kg/s]	26.5		
	Total molten mass during t _m [kg]	64,000		
Corium release to the containment				
Time interval	0 to 10 min after RPV failure	During 10 min interval at highest release rate of oxidic phase		
Total mass released to the containment [kg]	62,000 (68,000 ^a , 55,000 ^b)	11,000		
Total volume released to the containment [m ³]	8.5 (9.3 ^a , 7.6 ^b)	1.3		
Rate of release [m ³ /min]	0.56 (0.62 ^a , 0.50 ^b)	0.13		
Composition of corium released onto drywell floor [% mass]	metal	oxide	metal	oxide
	91 (92 ^a , 90 ^b)	9 (8 ^a , 10 ^b)	72	28

(a) and (b), respectively, refer to the upper and lower bounds of mass released.

Results

Table 2.8. Summary of APRIL.MOD3 results for Case 5

Time of RPV failure		194 min		
Melting of RPV upper structures				
Melting of RPV upper structures	Duration of high melting period, t_m [min]	30		
	Average melting rate during t_m [kg/s]	36		
	Total molten mass during t_m [kg]	64,000		
Corium release to the containment				
Time interval	0 to 10 min after RPV failure	During 10 min interval at highest release rate of oxidic phase		
Total mass released to the containment [kg]	70,000 (98,000 ^a , 41,000 ^b)	6,000		
Total volume released to the containment [m ³]	8.0 (12.7 ^a , 5.3 ^b)	0.7		
Rate of release [m ³ /min]	0.80 (1.27 ^a , 0.53 ^b)	0.07		
Composition of corium released onto drywell floor [% mass]	metal	oxide	metal	oxide
	94 (96 ^a , 92 ^b)	6 (4 ^a , 8 ^b)	48	52

(a) and (b), respectively, refer to the upper and lower bounds of mass released.

Table 2.9. Summary of APRIL.MOD3 results for Case 6

Time of RPV failure		234 min		
Melting of RPV upper structures				
Melting of RPV upper structures	Duration of high melting period, t_m [min]	30		
	Average melting rate during t_m [kg/s]	36		
	Total molten mass during t_m [kg]	65,000		
Corium release to the containment				
Time interval	0 to 10 min after RPV failure	During 10 min interval at highest release rate of oxidic phase		
Total mass released to the containment [kg]	59,000 117,000 ^a , 0.0 ^b)	11,000		
Total volume released to the containment [m ³]	8.0 (16.0 ^a , 0.0 ^b)	1.2		
Rate of release [m ³ /min]	0.8 (1.6 ^a , 0.0 ^b)	0.12		
Composition of corium released onto drywell floor [% mass]	metal	oxide	metal	oxide
	100 (100 ^a , -- ^b)	0 (0 ^a , -- ^b)	50	50

(a) and (b), respectively, refer to the upper and lower bounds of mass released.

Table 2.10. Summary of APRIL.MOD3 results for Case 7

Time of RPV failure		207 min		
Melting of RPV upper structures				
Melting of RPV upper structures	Duration of high melting period, t_m [min]	30		
	Average melting rate during t_m [kg/s]	36.5		
	Total molten mass during t_m [kg]	66,000		
Corium release to the containment				
Time interval	0 to 10 min after RPV failure	During 10 min interval at highest release rate of oxidic phase		
Total mass released to the containment [kg]	71,000 (95,000 ^a , 47,000 ^b)	13,000		
Total volume released to the containment [m ³]	9.7 (13.2 ^a , 6.1 ^b)	1.5		
Rate of release [m ³ /min]	0.66 (0.90 ^a , 0.41 ^b)	0.15		
Composition of corium released onto drywell floor [% mass]	metal	oxide	metal	oxide
	95 (97 ^a , 93 ^b)	5 (3 ^a , 7 ^b)	57	43

(a) and (b), respectively, refer to the upper and lower bounds of mass released.

Table 2.11. Summary of APRIL.MOD3 results for Case 8

Time of RPV failure		116 min		
Melting of RPV upper structures				
Melting of RPV upper structures	Duration of high melting period, t _{rn} [min]	13		
	Average melting rate during t _m [kg/s]	53.5		
	Total molten mass during t _m [kg]	42,000		
Corium release to the containment				
Time interval	0 to 10 min after RPV failure	During 10 min interval at highest release rate of oxidic phase		
Total mass released to the containment [kg]	59,000 (76,000 ^a , 42,000 ^b)	23,000		
Total volume released to the containment [m ³]	8.1 (10.8 ^a , 5.3 ^b)	2.3		
Rate of release [m ³ /min]	0.81 (1.10 ^a , 0.53 ^b)	0.15		
Composition of corium released onto drywell floor [% mass]	metal	oxide	metal	oxide
	92 (94 ^a , 90 ^b)	8 (6 ^a , 10 ^b)	5	95

(a) and (b), respectively, refer to the upper and lower bounds of mass released.

Table 2.12. Effective resistance of the upper structures for heat radiation from the core and hot gas

Thermal emissivity			Effective resistance for radiation
Core upper surface	Upper shroud	Gas	
0.5	0.5	0.2	3.1
0.5	0.5	0.8	3.7
0.5	0.3	0.2	4.4
0.5	0.3	0.8	5.0
0.5	0.2	0.2	6.1
0.5	0.2	0.8	6.7
0.3	0.2	0.2	7.4
0.3	0.2	0.8	8.0

Based on the above discussion, an effective resistance for radiation, $R_{\text{eff}} = 5$ (see Table 2.12), was used in Equation (12) to obtain estimates of the heating rate of the upper structures. In these calculations, the structure temperature was taken as the melting temperature of steel (1,783°K) and the heat radiation area from the degraded core was assumed to be 10 m² (which corresponds to a diameter of 3.57 m, or 11'8"). The calculations were performed for various assumed temperatures of the top surface of the degraded (and partially molten) core, are shown in Table 2.13.

The results given in Table 2.13 clearly indicate that the combined effects of heat radiation directly from the core and from the flowing gas produce a significant heatup rate of the upper structures. It is interesting to notice that the time needed to completely melt the shroud head (the mass of

which was 15,000 kg in the present calculations) would be only about 57 min (from the moment when the shroud temperature reached the steel melting temperature} if the core surface temperature stayed close to the zircaloy melting temperature (1,853°C), and less than 22 min if the core temperature was 2,200°C.

Even though the present estimates are based on several simplifying assumptions (in particular, they do not include the effect of convective heat transfer from the steam/gas mixture, which may become significant during the periods of high flow rate combined with increased oxidation-induced in-core heatup of the mixture), its results are consistent with the APRIL.MOD3 predictions. This can be readily seen by comparing the results in Table 2.13 against the rates of upper structure melting shown in Figure 2.4 and Table 2.4.

Table 2.13. Heating and melting rates of the upper in-vessel structures for various temperatures at the top of the core

Temperature of the top of the core [K]	Heating rate of the upper structure [MW]	Steel melting rate [kg/s]
3073	9.0	33.8
2773	5.6	20.8
2473	3.1	11.6
2125	1.2	4.4

8.0 Concluding Remarks

The APRIL.MOD3 code has been used to simulate the progression of core meltdown during short-term station blackout accidents in a typical BWR/4 reactor with MARK-I containment. The particular input deck for the APRIL calculations was prepared based on the Peach Bottom data and the postulated pre-accident conditions and ADS actuation timing similar to those used before in BWR SAR calculations. In addition to the reference accident scenario, several APRIL.MOD3 runs were performed in order to test the sensitivity of results to various input parameters and modeling assumptions and to quantify some of the major uncertainties in the modeling of severe accident phenomena.

In addition, stand-alone parametric calculations were performed which confirmed the order of magnitude of the APRIL-calculated melting rates of the upper vessel internals due to heat radiation from the degraded and partially molten reactor core.

The obtained results can be summarized as follows:

1. The depressurization of the reactor pressure vessel when the core is already partially uncovered leads to a sudden and complete core dryout. As a result of this, the water level drops below the lower core plate, which in turn minimizes the evaporation rate from the pool and practically stops oxidation of the metallic core constituents. Consequently, the decay heat power becomes the only source of energy, and the core heatup process is much slower compared to the scenario without RPV depressurization.
2. The gradual heating of the entire core (central zones first) prevents a substantial crust formation and channel blockage, so that molten materials (zircaloy and steel, in particular) are released onto the lower core plate and, after the plate failure, into the lower plenum.
3. The almost-adiabatic mode of core heatup results in a relatively uniform (axially) temperature increase in the core, especially—in its upper regions. With the increasing corium temperature the effect of heat radiation from the top of the core becomes more significant and the heatup of the top-most regions slows down compared with the other upper and central parts of the core.
4. The heat radiation from the top of the core substantially contributes to the heatup of the upper vessel internals (steam separator/dryer complex). It should be pointed out, however, that the temperature increase of the shroud head, standpipes and steam separator is controlled by two interrelated processes: direct heat radiation from the top of the core and a combined heat radiation/convection from the hot gas flowing from the core upwards. As long as the evaporation of water from the pool in the lower plenum is very low, the direct surface-to-surface radiation has a dominant effect. This situation changes dramatically when the core melting progresses to the extent that the lower core plate fails and molten corium is released into the lower plenum. The resultant evaporation, and the associated oxidation of the remaining zircaloy, increase the temperature of the steam/hydrogen mixture at core exit. The energy transfer from the gas to the upper structures via radiation and convection becomes an important (or even dominant) factor in the heatup of the latter. During this phase of an accident, the thermal power received by the upper structures may become very high, resulting in fast melting of large quantities of steel.

5. Due to the complexity of geometry involved and the material properties which can only be roughly estimated, the modeling of heat radiation in a degraded core is inherently associated with substantial uncertainties. Hence, it was deemed important that the effect of radiation be parametrically tested in the present study. Specifically, two cases were investigated using the APRIL.MOD3 code. In one of them, the radiation model in APRIL was used in its original form. In the other, the direct radiation from the core towards the upper structures was reduced by a factor of ten. Whereas the observed differences in the results were consistent with the anticipated trends, the overall molten mass of the upper structures experienced only a moderate change. This can be explained by the fact that during the phase of intensive heatup and melting of the steam separator/dryer complex the most important mode of heat transfer into the structures is via heat convection/radiation from the steam flowing from the core towards the steam separator (the steam/gas flow rate is enhanced by a closed-loop natural circulation formed inside the vessel ('core'-'steam separator'-downcomer'-lower plenum'-'core')). It is important to mention that this result of APRIL.MOD3 calculations agrees well with the parametric estimates performed using a separate stand-alone model described in Section 6.0.
6. The composition of corium released from the reactor pressure vessel into the drywell depends on the time of vessel failure. In the calculations performed in this study, APRIL.MOD3 predicted an early local failure of the lower head penetrations (in particular, of the lower head plug). In such a case, the mass of molten UO_2 at the time of RPV failure is relatively small. The amount of corium released immediately after the failure, and the associated rate of release, depend on two major factors: (a) the amount of molten metals in the lower plenum at the time of failure, and (b) the rate of in-vessel melting immediately after the failure. In the present analysis, two bounding cases have been considered for each calculated accident progression sequence. In one of them the pre-failure melt was assumed to be completely solidified, in the other—in the liquid form. In both cases, the predictions show that the melt released from the vessel during the initial time period after the failure (varying from 10 to 15 min) was predominantly metallic. The results for both cases have been subsequently used to obtain average release rates for each scenario (Cases 1 through 8). The total initial release in the base case (Case 1) was 35,000 kg (less than 5 m^3) of 94 percent metallic corium, at a rate of $0.5 \text{ m}^3/\text{min}$. The maximum total initial release was obtained in Case 7: 71,000 kg (9.9 m^3) of 95 percent metallic corium at a rate of $0.66 \text{ m}^3/\text{min}$. On the other hand, the highest content of oxidic phase during the period of initial release was 13 percent (Case 3), but the associated total release and release rate were only 53,000 kg (7.3 m^3) and $0.5 \text{ m}^3/\text{min}$, respectively. For a comparison, the major assumptions in NUREG/CR-5423 [Theofanous et al., 1990] were: in Scenario I - 12.5 m^3 of 90 percent⁺ oxidic corium at a rate of $0.5 - 4.0 \text{ m}^3/\text{min}$; Scenario II - 14 m^3 of corium containing 10 percent of oxidic phase released at a rate of $0.7 \text{ m}^3/\text{min}$.
7. Since the core heatup process after the RPV failure and lower plenum dryout is due to the decay heat only, the rates of corium melting and melt release into the containment are much lower than during the initial period after failure. During this time, however, the composition of corium gradually changes and its oxidic fraction increases. In the base case, the highest percent of oxidic phase in the corium being released from the RPV (after about 5 hr from the beginning of accident) was 25 percent, but the melt flow rate was

Concluding

only $0.16 \text{ m}^3/\text{min}$. Of all the scenarios with system depressurization (Cases 1-7) the highest content of oxide during a late release phase was about 50 percent at a rate of less than $0.15 \text{ m}^3/\text{min}$.

8. In order to see the effect of RPV failure time on the composition and rate of the corium released into the containment, calculations were performed in which the originally-calculated failure time was delayed. As can be seen in Appendices C and D, and in Tables 2.6 and 2.7, whereas the calculated: initial releases, release rates, and oxide content, of corium tend to grow with the increasing failure time, the observed changes are relatively slow and the conclusions drawn in regard to the base case remain valid even if the reactor vessel does not fail until about 4 hr after the beginning of core uncovering.
9. A similar conclusion applies to the results of calculations in which the effect was tested of axial power profile in the core.
10. Whereas the calculations for various options of a short-term station blackout accident with system depressurization performed according to the current accident management procedures gave similar results and led to similar conclusions concerning the anticipated composition and amount of melt released into the MARK-I containment, the calculation performed for a high pressure accident scenario (ie, without ADS) indicates that the accident consequences in the latter case would be significantly different. As the results shown in Appendix H and Table 2.11 indicate, in the case without depressurization the core meltdown process accelerates, and the corium released into the containment may contain a larger fraction of the oxidic constituent than in any other cases investigated in this work. This, in turn, confirms the importance of including

vessel depressurization in BWR accident management strategies.

Final Conclusions

1. The objective of the present study was to analyze the effect of heat transfer from a degraded and partially molten BWR core towards the upper internals in the reactor pressure vessel, and of the resultant melting of these structures, on the composition of corium released into the drywell following vessel failure. The numerical simulations for this study were performed using the APRIL.MOD3 computer code, with the Peach Bottom BWR as a reference power plant with Mark-I containment. The accident conditions analyzed assumed system depressurization via ADS performed according to the Peach Bottom emergency procedure guidelines (EPG).
2. Scenario-I in the NUREG/CR-5423 report assumes the release of a large amount of mainly oxidic corium over a short period of time. Practically speaking, such a release is possible only when a molten debris pool is formed inside the core. If water is still available in the vessel, the heat transfer from the bottom, sides and top of the molten region may be sufficient to form a crust confining the pool but insufficient to cool down the melt. The melting and heatup processes may continue for a long time, so that the accumulated melt may eventually amount to a substantial fraction of the total fuel inventory in the core (see the TMI accident). It should be mentioned here that the phenomena of corium solidification and crust/molten-corium-pool formation are modeled in APRIL.MOD3. However, according to the APRIL.MOD3 predictions reported in the present study, following system depressurization via ADS the water level in the BWR reactor vessel quickly falls below the bottom of the core,

and even below the lower core plate. As a result of this, the core heatup process occurs rather uniformly, and the various molten materials gradually relocate along the lower portions of the core and fall into the water in the lower plenum (prior to reactor vessel failure) or are released into the containment (after vessel breach). Naturally, the melting process is enhanced by the heat of oxidation reaction between the metallic zircaloy in the core and steam from melt quenching in the lower plenum. Hence, if the corium release onto the reactor vessel lower head directly leads to vessel failure (which is the case in both the NUREG/CR-5423 Scenario-I and the present simulations), the APRIL.MOD3 predicted release of molten oxidic corium into the containment is much smaller than the maximum Scenario-I-postulated value in NUREG/CR-5423.

3. Heat radiation from the core and combined convective/radiative heat transfer from the hot steam/gas mixture may have a very significant effect on the heatup and melting of the upper structures (steam separator/dryer complex) and, consequently, may add a large amount of molten steel to the molten debris accumulated on the reactor vessel lower head. Hence, the molten steel is likely to dominate the composition of corium released after vessel failure. The contribution of the oxidic phase may substantially increase during the long-term release through the vessel breach, but the volumetric flow rate of the melt at that time is expected to be small. On the other hand, it can be readily deduced from the

results of the present study that if the melting of the upper structures was not accounted for, the composition of corium released onto the containment floor would be significantly different (the fraction of oxidic constituents would be much higher, although the metallic phase could still account for as much as 50 percent). At the same time, however, the release rates would be lower by at least one order of magnitude compared to the current results of APRIL.MOD3 simulations.

4. It is widely recognized that the modeling of core meltdown phenomena in existing severe accident codes (including APRIL.MOD3) is still associated with substantial uncertainties. Examples include: spatial effects of heat transfer (in particular, heat radiation) and heatup in a degraded core, eutectic interactions and the resultant changes in material properties, the effect of core geometry deformation and local steam availability on the extent of zircaloy oxidation, melt coolability, core blockage, etc. Nevertheless, the results of the present analysis indicate that even if the calculated oxidic debris releases were substantially increased (i.e., by 50 percent or more), the overall melt release should still stay below the bounding conditions used in NUREG/CR-5423. It should be stressed here that this conclusion applies only to BWR core meltdown accidents with timely system depressurization. According to the statement of work, the present study was specifically directed towards the analysis of such a class of severe accidents.

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Appendix A

Results of APRIL MOD3 Simulations of a Short-Term Station Blackout at Peach Bottom: Case 1

Figure A-1. Nodal fuel temperatures in the central radial zone for Case-1 scenario. 1 - bottom axial node, 8 - top axial node.

Figure A-2. Water level in the reactor vessel for Case-1 scenario

Figure A-3. Temperature of the upper structures for Case-1 scenario. US - upper shroud, SP - standpipes, SS - steam separator, SD - steam dryer.

Figure A-4. Molten mass of steel from control blades and upper structures for Case-1 scenario.
CR - control rods, US - upper structures.

**Figure A-5. Mass of corium in the containment; upper bound calculations for Case-1 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.**

Figure A-6. Mass of corium in the containment; lower bound calculation for Case-1 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.

Appendix B

Results of APRIL MOD3 Simulations of a Short-Term Station Blackout at Peach Bottom: Case 2

Figure B-1. Nodal fuel temperatures in the central radial zone for Case-2 scenario. 1 - bottom axial node, 8 - top axial node.

Figure B-2. Water level in the reactor vessel for Case-2 scenario

Figure B-3. Temperature of the upper structures for Case-2 scenario. US - upper shroud, SP - standpipes, SS - steam separator, SD - steam dryer.

Figure B-4. Molten mass of steel from control blades and upper structures for Case-2 scenario. CR - control rods, US - upper structures.

**Figure B-5. Mass of corium in the containment; upper bound calculation for Case-2 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.**

Figure B-6. Mass of corium in the containment; lower bound calculation for Case-2 scenario. 1 - steel, 2 - zircaloy, 3 - UO₂.

Appendix C

Results of APRIL MOD3 Simulations of a Short-Term Station Blackout at Peach Bottom: Case 3

Figure C-1. Nodal fuel temperatures in the central radial zone for Case-3 scenario. 1 - bottom axial node, 8 - top axial node.

Figure C-2. Water level in the reactor vessel for Case-3 scenario

Figure C-3. Temperature of the upper structures for Case-3 scenario. US - upper shroud, SP - standpipes, SS - steam separator, SD - steam dryer.

Figure C-4. Molten mass of steel from control blades and upper structures for Case-3 scenario.
CR - control rods, US - upper structures.

**Figure C-5. Mass of corium in the containment; upper bound calculation for Case-3 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.**

Figure C-6. Mass of corium in the containment; lower bound calculation for Case-3 scenario. 1 - steel, 2 - zircaloy, 3 - UO₂.

Appendix D

Results of APRIL MOD3 Simulations of a Short-Term Station Blackout at Peach Bottom: Case 4

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Figure D-2. Water level in the reactor vessel for Case-4 scenario

Figure D-3. Temperature of the upper structures for Case-4 scenario. US - upper shroud, SP - standpipes, SS - steam separator, SD - steam dryer.

Figure D-4. Molten mass of steel from control blades and upper structures for Case-4 scenario. CR - control rods, US - upper structures.

Figure D-5. Mass of corium in the containment; upper bound calculation for Case-4 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.

Figure D-6. Mass of corium in the containment; lower bound calculation for Case-4 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.

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Results of APRIL MOD3 Simulations of a Short-Term Station Blackout at Peach Bottom: Case 5

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Figure E-2. Water level in the reactor vessel for Case-5 scenario

Figure E-3. Temperature of the upper structures for Case-5 scenario. US - upper shroud, SP - standpipes, SS - steam separator, SD - steam dryer.

Figure E-4. Molten mass of steel from control blades and upper structures for Case-5 scenario. CR - control rods, US - upper structures.

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**Figure E-5. Mass of corium in the containment; upper bound calculation for Case-5 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.**

Figure E-6. Mass of corium in the containment; lower bound calculation for Case-5 scenario. 1 - steel, 2 - zircaloy, 3 - UO₂.

Appendix F

Results of APRIL.MOD3 Simulations of a Short-Term Station Blackout at Peach Bottom: Case 6

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Figure F-2. Water level in the reactor vessel for Case-6 scenario

Figure F-3. Temperature of the upper structures for Case-6 scenario. US - upper shroud, SP - standpipes, SS - steam separator, SD - steam dryer.

Figure F-4. Molten mass of steel from control blades and upper structures for Case-6 scenario. CR - control rods, US - upper structures.

**Figure F-5. Mass of corium in the containment; upper bound calculation for Case-6 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.**

Figure F-6. Mass of corium in the containment; lower bound calculation for Case-6 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.

Appendix G

Results of APRIL.MOD3 Simulations of a Short-Term Station Blackout at Peach Bottom: Case 7

**Figure G-1. Nodal fuel temperatures in the central radial zone for Case-7 scenario.
1 - bottom axial node, 8 - top axial node.**

Figure G-2. Water level in the reactor vessel for Case-7 scenario

Figure G-3. Temperature of the upper structures for Case-7 scenario. US - upper shroud, SP - standpipes, SS - steam separator, SD - steam dryer.

Figure G-4. Molten mass of steel from control blades and upper structures for Case-7 scenario.
CR - control rods, US - upper structures.

Figure G-5. Mass of corium in the containment; upper bound calculation for Case-7 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.

Figure G-6. Mass of corium in the containment; lower bound calculation for Case-7 scenario. 1 - steel, 2 - zircaloy, 3 - UO₂.

Appendix H

Results of APRIL.MOD3 Simulations of a Short-Term Station Blackout at Peach Bottom: Case 8

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Figure H-2. Water level in the reactor vessel for Case-8 scenario

Figure H-3. Temperature of the upper structures for Case-8 scenario. US - upper shroud, SP - standpipes, SS - steam separator, SD - steam dryer.

Figure H-4. Molten mass of steel from control blades and upper structures for Case-8 scenario. CR - control rods, US - upper structures.

Figure H-5. Mass of corium in the containment; upper bound calculation for Case-8 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.

Figure H-6. Mass of corium in the containment; lower bound calculation for Case-8 scenario.
1 - steel, 2 - zircaloy, 3 - UO₂.

**PART III:
AUDIT CALCULATIONS WITH CORCON-MOD 3
OF THE DURATION OF SUPERHEAT
IN NUREG/CR-5423**

Abstract

Analyses (T.G. Theofanous et al., The Probability of Liner Failure in a Mark-I Containment, NUREG/CR-5423, July 1989, draft for comment) have shown that early rupture of the Mark-I boiling water reactor containment by the direct action of core debris depends strongly on the time that core debris is superheated above its liquidus. The analyses of the duration of superheat in the core debris are compared to predictions obtained with the CORCON-MOD 3 computer code. The predictions of this computer code as functions of the core debris mass, composition, and initial superheat are used to create a polynomial response surface. This response surface is used in a Monte Carlo analysis to produce probability distributions for the duration of superheat in core debris in the drywell of a Mark-I containment. It is concluded that to a high level of confidence (>90 percent) the duration of superheating predicted with the CORCON-MOD 3 code is less than what has been used in the analyses of the threats to the Mark-I containment liner. Based on these results, to the extent superheat duration dictates the threat to the liner, analyses in NUREG/CR-5423 would appear to overestimate the threat to the liner in comparison to threats that would be estimated using the predictions of the duration of superheating obtained with CORCON-MOD 3.

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Executive Summary

In response to review comments concerning the report The Probability of Liner Failure in a Mark-I Containment (NUREG/CR-5423), calculations of the duration of melt superheat during the interaction of core debris with concrete were done with the state-of-the-art model CORCON-MOD 3. Results for 17 calculations were used to construct a response surface for the duration of superheating divided by initial melt depth in terms of the melt depth, the initial superheat of the melt, and the fraction of zirconium metal in the debris. Uncertainty distributions for these independent variables were taken from NUREG/CR-5423 and used in a Monte Carlo analysis of the response surface to determine the uncertainty distribution in values of the duration of melt superheat divided by melt depth. The uncertainty distribution indicates that to a confidence level of more than 90 percent values of the duration of superheat divided by initial melt depth calculated with CORCON-MOD 3 are smaller than values used in NUREG/CR-5423. It is concluded that conservative values of the duration of superheat have been used in the analyses reported in NUREG/CR-5423.

1.0 Task Statement

1.1 Background

The vulnerability of the Mark-I containment for boiling water reactors to attack by core debris expelled from the reactor coolant system was identified by Greene and coworkers in 1983 [1,2]. These investigators showed using the CORCON-MOD 1 code [3] and ancillary analyses that, in the absence of a water pool, core debris would rapidly heat the Mark-I liner to melting in most cases. These early results have largely been confirmed in subsequent calculations [4,5] at least for melts deeper than about 5 cm. Expert elicitations on the issue of liner failure for the so-called "NUREG-1150 effort" [6] indicated a conditional probability of liner failure by core debris attack when no water is present of between 60 and 80 percent.

Mitigation of the liner attack has become of concern [7]. The most frequently cited basis for mitigating the debris attack on the liner is to assure a pool of water is present and maintained in the drywell when core debris is expelled from the reactor coolant system. Water has been found to do more than just extract heat from the core debris. The liner above the core debris would be in contact with the water. The liner would then behave like a cooling fin. Heat imparted into the liner from core debris would be conducted away by the cooling fin. It has been contended that this cooling would be sufficient to prevent liner failure at least for several hours.

There has not been universal agreement that water would be sufficient to prevent liner failure. Expert elicitations in the "NUREG-1150 effort" indicated the conditional probability for liner failure when water is present is on average 50 percent with essentially two points of view. In one view, water virtually assures no liner failure. In the other view, the water pool is only a slight perturbation and there is little likelihood that the liner will not be promptly penetrated by the core debris despite the presence of water.

The USNRC has commissioned a more definitive examination of the liner failure issue and the effects of water, to be done by Professor T. G. Theofanous and coworkers [8]. A review of this effort showed that core debris interactions with concrete played a critical role in the quantitative analysis of liner behavior. Analyses of these interactions were done by Professor Theofanous and coworkers using a computer code they developed. The analyses they did of liner failure indicated that the time following core debris expulsion from the reactor coolant system during which molten core debris is superheated is an important factor in the prediction of the vulnerability of the Mark-I liner. They concluded that this so-called "duration of superheat" is dependent on:

- the mass of melt expelled into the drywell,
- the superheat of the melt when it was expelled into the drywell, and
- the amount of metallic zirconium present in the core debris.

These quantities are uncertain in reactor accidents. Theofanous and coworkers developed subjective probability distributions for these three variables. They then used these distributions to construct a probability distribution for the duration of superheat divided by melt depth. This was done for two hypothesized scenarios:

Scenario I: A massive amount of largely oxidic melt is expelled from the RCS at the time of vessel failure.

Scenario II: A relatively small amount of largely metallic melt is expelled from the RCS at the time of lower head failure. The remainder of the core melt is slowly expelled over many hours.

These two scenarios correspond to two different descriptions of the progression of core

Task Statement

degradation in the boiling water reactor coolant system (RCS). The first, usually attributed to the MAAP [9] or the MARCH [10] codes, envisages a degradation scenario rather similar to that observed in the Three Mile Island accident involving a pressurized water reactor. Core debris accumulates within a frozen crust. Eventually this crust ruptures, the melt spills out, fails the lower head probably at a penetration and melt cascades into the drywell of the reactor. The second scenario, usually ascribed to the BWR SAR code [11], envisages debris being quenched when it falls into the lower plenum of the reactor vessel. Once all water in the plenum has boiled away, the fragmented debris reheats. Materials melt and relocate when temperatures exceed their melting points. The first materials to melt are the metals and the metallic melt penetrates the reactor head. The liner of the Mark-I BWR would in this scenario be exposed to metallic melt initially.

Reviewers of the work expressed an interest in a comparison of the melt/concrete interaction results obtained by Theofanous and coworkers with results that would be obtained with the CORCON-MOD 3 computer code which is an advanced model of core debris interactions with concrete. The task undertaken here is to develop a probability distribution for the duration of superheat divided by melt depth using CORCON-MOD 3. This is to be done for Scenario I above.

1.2 Definition of Superheat

Superheat in a pure system is well defined as the difference between the melt temperature and the melting point of the material. Superheat is less easily defined in a mixture because there is a temperature range of melting rather than a single melting temperature. Superheat could be defined as the temperature above either the liquidus or the temperature above the solidus. In most metallic systems there would not be a vast numerical difference in these two definitions of superheat--ca. 20 K. In oxide systems, the two

definitions could yield numerical differences of hundreds of degrees.

Superheat becomes even more difficult to define in the case of a molten mixture interacting with solids. The reference temperature for superheat can be taken as the solidus of the melt, the liquidus of the melt, or the melting point of the solid. Interactions of melts with concrete raise other possible reference points. The ablation temperature of the concrete [3] could be taken as a reference point for defining superheat. Also, since heat transfer by convection is so important in such systems, a reference temperature could be defined as the temperature at which the viscosity of a two-phase (solid plus liquid) melt is so high that heat transfer is predominantly by conduction.

Careful discussions were held between one of the authors of this letter report (DAP) and Professor Theofanous on this issue of superheat definition. It was established that the definition of superheat adopted by Theofanous and coworkers is the difference between the liquidus temperature and the melt temperature. The duration of superheat is the time required for the melt to fall to the liquidus the first time. This is the definition adopted here. Calculations are also reported for two other definitions for the interests of others. In these other definitions, superheat is defined as:

- The difference between the melt temperature and the average of the solidus and liquidus, and
- The difference between the melt temperature and the liner melting point which is taken to be 1700 K.

In examining results of the audit analysis it should be born in mind that the liquidus temperature for superheat calculation is time dependent. The time dependence arises because the liquidus and solidus temperatures change as the condensed products of concrete decomposition are incorporated into the melt. A schematic diagram of the

temperature of the melt calculated by CORCON as a function of time is shown in Figure 3.1. Also shown are the liquidus and solidus of the melt and several important points:

(t_0, T_0) initial time and melt temperature

$(t^*, T_L^{(1)})$ time at which the melt first reaches the liquidus temperature

$(t^{*'}, T_L^{(2)})$ time at which the melt temperature again reaches the liquidus

$(t^{**}, T_{1/2})$ time at which the melt temperature reaches the numerical average of the liquidus and solidus temperatures.

Figure 3.1. Time variation of melt temperature and the solidus and liquidus temperature during oxidic core debris interactions with concrete

2.0 Objective of the Audit Calculations

The objective of these audit calculations is to satisfy reviewer concerns about the duration of superheat used in analysis of probability of liner failure in NUREG/CR-5423 [8]. To do this, a probability distribution of the duration of superheat is to be developed from predictions of the CORCON-MOD 3 code considering as uncertain inputs:

- melt depth (D)
- initial superheat of the melt (ΔT)

- fraction of the zirconium in the metallic state in the melt initially (ω)

Melt depth is related to the volume of melt assumed to be deposited into the drywall by the formula:

$$D(\text{cm}) = 1.83 V(\text{m}^3) - 10.51$$

where V is the melt volume. This formula accounts for holdup of melt in the drywell sumps.

3.0 Approach Used for the Audit Calculations

The audit analysis was confined to Scenario I as described in NUREG/CR-5423 [8]. Scenario I was selected because it is the scenario most easily reproduced using the CORCON-MOD 3 computer code. It is also the scenario that poses the greatest threat to the integrity of the Mark I liner. The approach adopted to audit the calculations was as follows:

- Use CORCON-MOD 3 to perform a set of calculations of melt/concrete interactions in the Peach Bottom Mark-I boiling water using several fixed values of the uncertain input parameters.
- Fit the results of the CORCON calculations to a simple equation using the uncertain inputs as independent variables.
- Consider the regression equation found by fitting the results of the CORCON calculations to define the response surface for the dependent quantity of interest.
- Use the response surface to calculate values of the dependent variable for randomly selected values of the three uncertain independent variables.
- Accumulate results of the calculations (Monte Carlo calculations) to define the uncertainty distribution of the dependent variable that arises from the uncertainties in the independent variables.
- Characterize the calculated distribution for the dependent variable so comparisons can be made to the distribution found in NUREG/CR-5423.

The specifications of the calculations done with CORCON-MOD 3 are shown in Table 3.1. The retained version of CORCON used for these

calculations is labelled CV281. Also shown in Table 3.1 are results of the calculations.

The results include:

t^*/D = duration of the superheat using the liquidus as the reference temperature for calculation of superheat divided by the initial, collapsed melt depth.

$t^{*'} / D$ = time at which melt temperature permanently falls below the liquidus of the melt (see text below) divided by the initial, collapsed melt depth.

t^{**} / D = duration of the superheat using the average of the solidus and liquidus as the reference temperature for superheat divided by the initial, collapsed melt depth.

t^{***} / D = duration of the superheat using 1700 K (approximately the liner melting point) as the reference temperature for superheat divided by the initial, collapsed melt depth.

Results of the CORCON calculations were fit to a polynomial expression in the dependent variables (D, and T) using a linear, least-squares, regression code. A least squares approach was adopted to avoid excessive parameterization of the polynomial expression and the attendant possibility of artificially introducing complexity to the response surface. Variables considered in the regression were:

1. ΔT	7. $\omega / \Delta T$	13. $(\omega / D)^2$
2. ω	8. $\Delta T / D$	14. ωD
3. D	9. $1 / (\Delta T)^2$	15. $(\omega D)^2$
4. $(\Delta T)^2$	10. $(\omega / \Delta T)^2$	16. $\Delta T D$
5. $(\omega)^2$	11. $(\Delta T / D)^2$	17. $1 / D$
6. D^2	12. ω / D	18. $(\Delta T D)^2$

Table 3.1. Matrix of CORCON calculations

#	ΔT (K)	ω (-)	D (cm)	t^*/D (s/cm)	$t^{*'} / D$ (s/cm)	t^{**}/D (s/cm)	t^{***}/D	t^*/D from NUREG/CR-5423 (s/cm)
1	10	0.05	2.3	0.43	0.43	18	112	1
2	150	0.05	2.3	1.3	1.3	7.8	86	6
3	10	0.40	2.3	0.4	0.4	9.1	47	1
4	10	0.05	20.6	0.3	0.3	27.7	83	1
5	150	0.40	2.3	1.3	0.3	83.5	195	16.5
6	150	0.05	20.6	4.1	4.1	17.4	141	6
7	10	0.40	20.6	0.15	38.0	66.7	132	1
8	150	0.40	20.6	1.7	41.0	62.3	147	16.5
9	40	0.14	11.4	1.9	1.9	36.8	139	2
10	40	0.40	11.4	1.3	42	71.8	139	2.5
11	10	0.14	20.6	0.26	0.26	34.9	131	1
12	40	0.14	20.6	1.45	1.45	34.1	103	2.0
13	40	0.40	20.6	0.44	40	70	170	2.5
14	150	0.14	11.4	6.1	--	31.4	173	8.5
15	75	0.14	11.4	2.63	--	34.2	202	4.5
16	75	0.14	20.6	4.75	--	31.9	139	4.5
17	75	0.40	20.6	0.73	39	70	139	5.5

 ΔT = initial superheat in melt ω = weight fraction Zr

D = pool depth

 t^*/D = BASE CASE duration of superheat divided by melt depth t^{**}/D = alternatively defined duration of superheat divided by melt depth

Approach

Selection of the useful variables was made by a classic F test of variances. It is imperative that it be borne in mind that no physical significance is ascribed to either the regression equation or the functional form of variables used in the fitting. Fitting was a completely empirical effort to define a response surface that could be rapidly evaluated in the Monte Carlo calculations.

The distribution functions for the independent variables were obtained from Professor Theofanous. These functions are listed in Table 3.2. Verification or evaluation of these distributions was not a part of this task. The functions are shown as probability densities and cumulative probability functions in Figures 3.2 to 3.4. The distribution functions are all piece-wise constant distributions which makes statistically appropriate Monte Carlo sampling easily done. Assume these are N ranges of values defined for the function. Each range is characterized by its end values. For the i^{th} range the end values are V_{i-1} and V_i . The probability that a value is within the range is

$$\Pr(V_{i-1} \leq V < V_i) = P(i).$$

Then the probability of a value less than or equal to V^* is:

$$\Pr(V \leq V^*) = \sum_{i=1}^M P(i) + \frac{P(M+1)(V^* - V_M)}{V_{M+1} - V_M}$$

such that $V_M \leq V^* < V_{M+1}$ and $V^* > V_i$ for $i \leq M$. By random selection, a probability is found. The appropriate value of M is selected. Inversion of the above formula yields the appropriate value for the independent variable V^* . This Monte Carlo selection, of course, has to be done independently for each of the three independent variables.

Values of the response function calculated from randomly selected values of the uncertain inputs were accumulated and used to define a distribution function. An important issue is how many Monte Carlo evaluations of the response function must be done to derive the distribution function for the response to a prescribed confidence level. The confidence, C, that a Monte Carlo sample of size n spans a fraction p of the range of values of the uncertain quantity is given by [14]:

$$C = 1 - np^{n-1} + (n-1)p^n$$

For the analyses reported here $n = 70000$. Then, there is a confidence of $C = 0.9999$ that a sample of size $n = 70000$ spans 0.9995 of the range of the uncertain variable. Values obtained in the Monte Carlo calculations were "binned" so that the resolutions in the BASE CASE and the case with an alternate definition of superheat described below were ± 0.05 and ± 0.5 s/cm, respectively.

Table 3.2. Probability distribution functions for the independent variables for Scenario I

VARIABLE: Melt Released (pdf1)*

Range (m ³)	Total Probability In the Range	Probability Density (m ⁻³)	Cumulative Probability
7-9	0.18	0.09	0.18
9-11	0.27	0.135	0.45
11-13	0.45	0.225	0.90
13-15	0.09	0.045	0.99
15-17	0.01	0.005	1.00

VARIABLE: Zirconium Content of the Melt (pdf3)*

Range (%)	Total Probability In the Range	Probability Density (-)	Cumulative Probability
0-10	0.10	0.01	0.10
10-20	0.79	0.079	0.89
20-30	0.10	0.01	0.99
30-40	0.01	0.001	1.00

VARIABLE: Initial Superheat (pdf4)*

Range (K)	Total Probability In the Range	Probability Density (K ⁻¹)	Cumulative Probability
0-25	0.045	0.0018	0.045
25-50	0.636	0.02544	0.681
50-75	0.182	0.00728	0.863
75-100	0.091	0.00364	0.954
100-125	0.036	0.00144	0.990
125-150	0.010	0.0004	1.000

*pdf1, pdf2, and pdf3 are identifiers used in Reference 8

Figure 3.2. Probability density and cumulative probability for melt released (pdf1)

Figure 3.3. Probability density and cumulative probability for zirconium content of the melt (pdf3)

Figure 3.4. Probability density and cumulative probability for the initial superheat of the melt (pdf4)

4.0 Results

4.1 BASE CASE

The BASE CASE considers superheat to be the temperature difference between the melt and the liquidus of the melt. This is the definition of superheat adopted in NUREG/CR-5423. Calculations with CORCON-MOD 3 indicate that the melt temperature very rapidly falls to the liquidus temperature. Especially for shallow melts, the time required for the melt temperature to drop to the liquidus is so small that results of CORCON calculations cannot be considered very accurate.

When a high temperature melt first contacts cold concrete, there are highly transient heat transfer processes initiated. Experimental evidence [12,13] shows that upon first contact and for a period of about 1 second there is no visual evidence of interaction. At the end of this induction period, a spallation event is often observed [12]. Available data [12] suggest that this spallation event involves the surface, cementitious material to a depth of about 0.4 cm. At the conclusion of this spallation event, the melt begins a more conventional ablative attack on the concrete.

Analysis of the initial contact between liquid melt and cold concrete suggests that the induction period involves the transient freezing of melt and conduction of heat into the concrete. Gases liberated from the concrete during this initial conduction period do not easily penetrate the still intact layer of cementitious material. Whether the spallation process is the result of pressurization near the cement-aggregate boundary immediately below the surface of the concrete or is the result of thermal stresses in the surface layer remains the object of debate.

It was long ago decided that the contact transient when melt first begins to interact with concrete need not be modeled in CORCON. This highly transient process contributes little to the major safety issues associated with core debris--concrete

interactions such as combustible gas generation and the depth of concrete ablation. CORCON-MOD 3 models do consider the possibility that an interfacial crust develops in the core debris when it first contacts concrete. The models, however, have been derived assuming a quasi-steady-state temperature distribution exists in the concrete. Surface cementitious material is assumed to be identical to bulk concrete and no attempt is made to model the spallation of concrete.

A usual time step for CORCON calculations is about 1 minute. A rule of thumb is that the first 4 or 5 time steps may not be very accurate because of the initial transient phenomena that are not modeled in the code. For the BASE CASE calculations, it is during this initial transient that bulk debris temperatures are predicted to fall to the mixture liquidus temperature. The possible inaccuracies of CORCON predictions during this initial period are neglected in the discussion below.

Results of the CORCON-MOD 3 calculations for the BASE CASE definition of superheat were fit by linear regression methods to the completely empirical expression:

$$0 \leq t^*/D \text{ (s/cm)} = -0.107271 + 1.346 \times 10^{-4} (\Delta T)^2 \\ + 979.349 (\omega/\Delta T)^2 \\ - 5.714 \times 10^{-4} (\Delta T/D)^2 \\ - 24.236 (\Delta/D)^2 \\ - 3.612 \times 10^{-2} (\Delta D)^2 \\ + 3.8464 \times 10^{-3} \Delta T D \\ - 1.1106 \times 10^{-6} (\Delta t d)^2$$

where

t^* = duration of the superheat

D = depth of the melt

ΔT = initial superheat

ω = fraction of metallic zirconium

Results

Predictions obtained with the regression equation and predictions of the CORCON code are compared in Figure 3.5. The regression equation accounts for about 93 percent of the variance in the CORCON predictions. The standard error in the regression equation is 0.66. The error amounts to only about 10 percent of the larger expected values of t^*/D . But, for very short durations of superheat, the regression equation is of low accuracy. The inaccuracies of the regression equation are similar to inaccuracies in the CORCON model that arise because of phenomena that are not modeled.

The regression equation was taken to define the CORCON-MOD 3 response surface. The distribution of values of t^*/D caused by uncertainties in inputs concerning initial superheat, melt depth and zirconium concentration was determined from 70000 Monte Carlo calculations. The probability density of t^*/D values is shown as a function of t^*/D in Figure 3.6. The complementary cumulative probability distribution is shown in Figure 3.7. Representative probability densities and cumulative probabilities are listed in Table 3.3.

Within the parlance developed in NUREG/CR-5423 particular significance is attached to values of t^*/D at complementary cumulative probabilities of 0.1, 10^{-2} , and 10^{-3} . The uncertainty ranges at various confidence levels for t^*/D values at these critical probabilities were calculated using non-parametric methods [14]. Results are shown in Table 3.4.

4.2 Conclusions Concerning the BASE CASE

It cannot be claimed that CORCON is an especially accurate model for the short initial period of interactions of melt with concrete. The modeling in CORCON is the best now available. Results obtained with CORCON indicate durations of superheat much less than durations of superheat found in NUREG/CR-5423. In some cases the differences amount to as much as a factor of 3. It can be concluded that for the

purposes of NUREG/CR-5423 the durations of superheat used in the analyses are conservative in the sense conservative is used in NUREG/CR-5423.

4.3 Excursions Beyond the Liquidus

The definition of the duration of superheat adopted in NUREG/CR-5423 is specifically the time after the contact of core debris with concrete that the bulk melt temperature first falls below the liquidus temperature. As noted in Table 3.1, in some, but not all, CORCON-MOD 3 calculations, the liquidus temperature fell below the melt temperature after the time defined as the duration of melt superheat. Eventually, the melt temperature again fell below the new, lower, liquidus temperature.

The authors do not believe the behavior observed in these calculations is unphysical. It is entirely possible that melt temperatures could exceed the liquidus temperature as the liquidus is reduced by condensed products of concrete decomposition. Whether it will actually occur as predicted by the CORCON code is debatable. Since the phenomenon did not occur in all calculations, no attempt was made to characterize the times at which the melt finally and permanently fell below the liquidus temperature.

4.4 Alternate Definition of Superheat

As discussed in earlier portions of this report, definitions of superheat different from that adopted in NUREG/CR-5423 are possible. Preference for one definition over another depends entirely on the heat transfer models being used to describe the response of the liner to heating by core debris. That is, a consistent description of superheat is all that is required.

An analysis is reported here for an alternate definition of superheat. The alternate definition is based on taking the average of the time-dependent liquidus and solidus as the reference temperature for superheat (of course, yet another

Figure 3.5. Comparison of BASE CASE t^*/D values calculated with CORCON and values calculated with the regression equation

Figure 3.6. Probability density of BASE CASE t^*/D values as a function of t^*/D

Figure 3.7. Complimentary cumulative probability distribution for the BASE CASE t^*/D . Critical probability values of 10^{-1} , 10^{-2} , and 10^{-3} are marked by the dashed lines

Results

Table 3.3. Results of BASE CASE Monte Carlo calculations

t^*/D (s/cm)	Probability density (cm/s)	Complementary cumulative probability
0.00 - 0.05	1.327×10^{-2}	0.99934
0.05 - 0.10	3.327×10^{-2}	0.99767
0.15 - 0.20	1.009×10^{-1}	0.98934
0.45 - 0.50	2.925×10^{-1}	0.92567
0.50 - 0.55	3.112×10^{-1}	0.91011
0.65 - 0.70	3.755×10^{-1}	0.85690
0.75 - 0.80	4.101×10^{-1}	0.81720
0.90 - 0.95	5.123×10^{-1}	0.74588
1.05 - 1.10	5.710×10^{-1}	0.66099
1.15 - 1.20	5.673×10^{-1}	0.60373
1.25 - 1.30	5.346×10^{-1}	0.54918
1.35 - 1.40	5.088×10^{-1}	0.49753
1.45 - 1.50	4.846×10^{-1}	0.44947
1.55 - 1.60	4.245×10^{-1}	0.40579
1.70 - 1.75	3.903×10^{-1}	0.34450
1.80 - 1.85	3.490×10^{-1}	0.30808
2.00 - 2.05	2.462×10^{-1}	0.25120
2.25 - 2.30	1.766×10^{-1}	0.20179
2.60 - 2.65	1.409×10^{-1}	0.15036
3.00 - 3.05	9.589×10^{-1}	0.10364
3.70 - 3.75	5.776×10^{-2}	0.05107
4.40 - 4.45	2.841×10^{-2}	0.02084
4.80 - 4.85	2.579×10^{-2}	0.01035
5.10 - 5.15	1.046×10^{-2}	0.00526
5.75 - 5.80	5.420×10^{-3}	0.00103

Table 3.4. Critical values of t^*/D for the BASE CASE

Complementary cumulative probability (description in NUREG/CR-5423)	Uncertainty range ⁺ for t^*/D (s/cm) at a confidence level of			
	50%	90%	95%	99%
0.5 (median)	1.35 - 1.40	1.35 - 1.45	1.35 - 1.45	1.35 - 1.45
0.1 (within known trends)	3.05 - 3.10	3.05 - 3.15	3.05 - 3.15	3.05 - 3.15
0.01 (outside spectrum of reason)	4.85 - 4.90	4.80 - 4.90	4.80 - 4.90	4.80 - 4.90
0.001 (physically unreasonable)	5.80 - 5.85	5.80 - 5.85	5.75 - 5.90	5.75 - 5.90

⁺ Note that the limit of resolution for these calculation is ± 0.05 (s/cm).

Results

alternative is to take the liner melting point as the reference temperature for superheat). This definition of superheat is of some significance for the analysis of core debris attack on concrete. Heat transfer from the bulk melt to the melt interface with concrete is a combination of conduction and convection. As solids precipitate as suspended crystals in the melt, the melt becomes more viscous. A point is reached at which the melt viscosity is high-enough that heat conduction predominates over convection as the mechanism for heat transfer to the interface. For simple two-phase slurry melts high viscosities develop when about 50 percent of the melt is suspended solids. To a crude approximation, this concentration of precipitated solids develops when the melt temperature is the average of the liquidus and solidus. This alternate definition of superheat may be of interest to those analyzing the Mark-I liner issue with different heat transfer models than those used in NUREG/CR-5423.

The authors emphasize that the discussions below do not relate to the analyses done in NUREG/CR-5423.

Results obtained with CORCON for the alternate definition of superheat are shown in Table 3.1. Note that the durations of superheat using the average of the liquidus and solidus as the reference temperature are an order of magnitude larger than durations obtained when the liquidus is used as the reference temperature. These results should be much less affected by transient phenomena that are not modeled in CORCON. The results were fit to the totally empirical regression expression:

$$\begin{aligned} t^{**}/D(\text{s/cm}) = & 22.835 + 173.597 \omega \\ & - 1278.5 (\omega/\Delta T) + 1620.6 (1/(\Delta T)^2) \\ & + 5.421 \times 10^{-3} (\Delta T/D)^2 \\ & - 5.563 \times 10^{-3} (\Delta T D) \\ & - 81.661 (1/D) \end{aligned}$$

where

t^{**} = duration of the superheat using the alternate definition of superheat

D = depth of the melt

ΔT = initial superheat

ω = fraction of metallic zirconium

Predictions obtained with the regression equation and predictions obtained with the CORCON code are compared in Figure 3.8. The regression equation accounts for more than 90 percent of the variance in the CORCON predictions. The standard error in the results is 9.95. On a relative basis this is about the same error as that obtained for the BASE CASE calculations.

The regression equation was taken to define the response surface for CORCON-MOD 3 for the alternate definition of superheat. The distribution of values of t^{**}/D caused by uncertainties in initial superheat, fraction of zirconium and melt depth was determined from 70000 Monte Carlo

Figure 3.8. Comparison of t^{**}/D values calculated with CORCON and values calculated with the regression equation

Results

calculations. Distributions for the uncertain inputs were taken to be the same as the distributions used for the BASE CASE calculations. The probability density of t^{**}/D values is shown as a function of t^{**}/D in Figure 3.9. The complementary cumulative probability distribution of t^{**}/D values is shown in Figure 3.10. Selected values of the probability density and the cumulative probability are shown in Table 3.5.

Note that even after 70000 calculations, the density distribution exhibits substantial "noise" for low probability combinations of input

parameters. Note also in the complementary cumulative probability distribution that one parameter (ω) is having a predominant effect on the values obtained for t^{**}/D . The step-wise nature of the distribution of this input parameter is reflected in the computed values of t^{**}/D .

Uncertainty ranges for t^{**}/D values at the critical probability values of 10^{-1} , 10^{-2} , and 10^{-3} are shown for various confidence levels in Table 3.6. The uncertainty ranges were found using non-parametric methods [14].

Figure 3.9. Probability density distribution of t^{**}/D as a function of t^{**}/D

Figure 3.10. Complimentary cumulative probability distribution for t^{**}/D (s/cm)

Table 3.5. Results of Monte Carlo calculations obtained for the alternate definition of superheat

t^{**}/D (s/cm)	Probability density (cm/s)	Complementary cumulative probability
2.00 - 2.50	3.429×10^{-4}	0.99916
7.50 - 8.00	1.571×10^{-3}	0.99487
10.00 - 10.50	2.200×10^{-3}	0.99023
13.50 - 14.00	3.771×10^{-3}	0.97963
18.50 - 19.00	9.657×10^{-3}	0.94940
22.00 - 22.50	1.588×10^{-2}	0.90300
24.50 - 25.00	2.297×10^{-2}	0.85100
28.00 - 28.50	4.046×10^{-2}	0.74186
29.00 - 29.50	4.797×10^{-2}	0.69539
30.00 - 30.50	5.303×10^{-2}	0.64314
31.00 - 31.50	5.311×10^{-2}	0.59030
31.50 - 32.00	5.309×10^{-2}	0.56376
32.50 - 33.00	5.231×10^{-2}	0.51281
33.50 - 34.00	5.137×10^{-2}	0.46150
34.50 - 35.00	5.051×10^{-2}	0.41153
35.50 - 36.00	5.2914×10^{-2}	0.36090
36.50 - 37.00	4.8314×10^{-2}	0.31226
38.00 - 38.50	4.5971×10^{-2}	0.24284
39.00 - 39.50	4.3114×10^{-2}	0.20034
40.00 - 40.50	3.9428×10^{-2}	0.16003
41.50 - 42.00	3.0486×10^{-2}	0.10936
48.50 - 49.00	5.8571×10^{-3}	0.04933
53.50 - 54.00	4.9714×10^{-3}	0.02041
56.00 - 56.50	3.0000×10^{-3}	0.01047
60.50 - 61.00	5.7143×10^{-3}	0.00514
68.50 - 69.00	4.0000×10^{-3}	0.00108

Table 3.6. Critical Values of t^{**}/D

Complementary cumulative probability	Uncertainty range ⁺ for t^{**}/D (s/cm) at a confidence level of			
	50%	90%	95%	99%
0.5 (median)	33.5 - 33.0	33.5 - 33.0	33.5 - 33.0	33.5 - 33.0
0.1 (within known trends)	42.5 - 42.0	42.5 - 42.0	43.0 - 42.0	43.0 - 42.0
0.01 (outside spectrum of reason)	57 - 56.5	57 - 56	57 - 56	57 - 56
0.001 (physically unreasonable)	57.5 - 56.5	57.5 - 56.5	57.5 - 56.5	57.5 - 56.5

⁺ Note that the limit of resolution for these calculation is ± 0.50 (s/cm)

5.0 Summary

The most important result reported here is that durations of superheat used in NUREG/CR-5423 are conservative, relative to predictions made with the CORCON code. That is, longer durations of superheat have been used in the analysis of liner failure than would be calculated with CORCON.

A non-parametric method is demonstrated for characterizing results of Monte Carlo uncertainty distributions. This method is applied to distributions found for the duration of melt superheat divided by melt depth for two definitions of melt superheat.

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PART IV:
COMPARISON OF MELTSPREAD-1 CALCULATIONS OF
CORIUM DEPTH NEXT TO THE MARK I LINER
WITH NUREG/CR-5423

Abstract

The MELTSPREAD-1 computer code has been used to calculate the depth of corium next to the containment steel liner for the three melt release scenarios treated by Theofanous et. al. in the report, "The Probability of Liner Failure in a Mark-I Containment," NUREG/CR-5423. Calculations were carried out for various combinations of released melt volume, melt superheat, and metallic zirconium content consistent with the probability density functions specified by Theofanous et. al. Additional calculations were performed to examine the impact of certain phenomenological uncertainties currently involved in the mechanistic prediction of melt spreading, namely the spread angle beyond the pedestal doorway, immobilization of the spreading materials, and heat losses from the spreading melt. Maximum corium depths adjacent to the shell were compared directly with Causal Relation One (CR1) from NUREG/CR-5423. For Scenario I, virtually all of the calculated peak corium depths lie within the range of CR1 or are conservatively bounded by CR1. Calculated peak depths exceed the 95 percentile bound of CR1 only for a released melt volume of about 9 cubic meters. However, the variance from the 95 percentile bound is small such that the depths do not differ by a significant amount. For both the peak release and averaged release variants of Scenario II, the calculated peak corium depths next to the liner are conservatively bounded by CR1. More specifically, for melt release conditions that correspond to the most probable values of the Scenario II probability density functions, the melt is calculated to freeze short of contacting the liner such that the peak depth is zero.

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1.0 Introduction

An assessment of the threat to the Mark I containment liner integrity in the event of melt release into the drywell during a postulated station blackout accident has recently been carried out by Professor T. G. Theofanous and co-workers in the study, "The Probability of Liner Failure in a Mark-I Containment," NUREG/CR-5423. The Theofanous study defined two classes of melt release from the reactor vessel by drawing upon the predictions of two differing in-vessel melt progression analysis methodologies, namely the MAAP code (rapid melt release with mainly oxidic content) and the Oak Ridge models (prolonged release with high initial metal content). The two classes of melt release were termed Scenario I and Scenario II, respectively. For Scenario II, two subcases were defined corresponding to melt release over a prolonged time at the average release rate ("averaged release" variant) and the release of a portion of the melt during the prolonged release at a temporarily greater peak release rate ("peak release" variant). The study investigated the effects of water being present inside the containment drywell for the alternative situations of a drywell floor flooded with water up to the spillover level into the vent pipes leading to the suppression pool (i.e., "wet" containment) and a "dry" floor without water.

For the combination of each scenario and drywell flooding condition, a probabilistic methodology was employed to arrive at the containment conditional failure probability in the event of melt release into the drywell. In order to render tractable the complex interrelated phenomena associated with melt release,

melt spreading, molten core-concrete interaction, and containment liner thermal attack, numerous conservative assumptions were introduced. To account for uncertainties in the melt release conditions, probability density functions were defined covering a range of released melt mass, zirconium metal content, and released melt superheat. The melt spreading behavior was described in terms of a "causal relation" designated as Causal Relation One (CR1). This causal relation is given by two curves providing as a function of melt volume released from the vessel the corium depths next to the liner corresponding to the 95 and 5 percentile values of a normal probability distribution for the corium depth. Different CR1s were specified by Theofanous et. al. for Scenarios I and II. However, for the peak release and averaged release variants of Scenario II, the same CR1 was used. Also, for each scenario, CR1 as quantified by Theofanous et. al. depended only upon the melt volume released from the vessel and not upon other conditions such as the released melt superheat or zirconium content. Two other causal relations were also defined to describe the melt interactions with the concrete substrate as well as overlying water and melt interactions with the liner.

Assuming that liner failure corresponds to thermal meltthrough, Theofanous et. al. calculated conditional probabilities for liner failure with a flooded drywell of 6×10^{-5} for Scenario I and 1.2×10^{-4} or 3.5×10^{-6} for the two Scenario II variants. These numbers correspond to "physically unreasonable" values on the probability scale that was used. In contrast, for the

case of a dry containment, conditional failure probabilities of 1 (Scenario I) and 0.63 (Scenario II) characterized as "a virtual certainty" were obtained. However, in the dry case, Professor Theofanous noted that his approach "may prove to be unduly conservative in not recognizing certain (unknown as yet) mitigating aspects of the complex phenomenology."

A computer code, MELTSPREAD-1, has been developed at Argonne National Laboratory under Electric Power Research Institute (EPRI) sponsorship to calculate the transient spreading of core and structural materials inside reactor containments (Farmer, Sienicki and Spencer, 1990). This mechanistic code incorporates models for the basic processes involved in the spreading of reactor materials over a steel or concrete substrate (e.g., Figure 4.1). The code currently accounts for: gravity-driven flow; melt freezing, immobilization, and remelting; concrete heatup, decomposition, and gas release; concrete melting, entrainment, and intermixing with the spreading melt; enhancement of heat transfer to overlying water or underlying concrete due to sparging decomposition gases; chemical oxidation of melt metallic constituents; spreading of melt over previously spread material; and two-dimensional heatup of the liner due to gas sparging-driven forced convection and impingement heat transfer from spreading melt adjacent to the liner. Two MELTSPREAD-1 calculations previously

carried out at the request of Professor Theofanous were included in Appendix G to NUREG/CR-5423.

As is the case with any code, a number of idealizations and approximations have been introduced in formulating the modeling incorporated into MELTSPREAD-1.

This reflects, in part, the current level of uncertainties associated with the calculation of spreading-related processes. Table 4.1 summarizes the major assumptions made in MELTSPREAD-1 together with an evaluation of how they impact upon the results of the present analysis. Additional information on the modeling incorporated in MELTSPREAD-1 and the numerical solution methodology can be found in the papers of Farmer, Sienicki and Spencer (1990) as well as a forthcoming EPRI code report.

The current analysis involves the use of MELTSPREAD-1 to mechanistically calculate the depth of corium next to the liner for the melt release scenarios specified by Theofanous et. al. The calculated depths are directly compared with CR1 for each scenario. The results of the present study thus provide a basis for confirming the conservatism incorporated into CR1 or indicating where potential variances from CR1 are calculated.

Figure 4.1. Illustration of Melt Spreading Processes Modeled in MELTSPREAD-1.

Table 4.1. Summary of Major Assumptions Incorporated into MELTSPREAD-1

Assumption	Evaluation
<p>Spreading is modeled within a prescribed one-dimensional channel defined on the drywell floor. Circular nodalization of pedestal; constant spreading angle between pedestal doorway and liner; and flow through the remainder of the drywell annulus are assumed for Mark I configuration.</p>	<p>The degree of approximation introduced by this assumption is dependent, among other things, upon how well the one-dimensional channel approximates the actual flow. The assumed flow configuration is qualitatively consistent with water spreading experiments of Theofanous et. al. conducted in a 1:10 scale Mark I geometry. Good agreement was obtained between MELTSPREAD-1 and measurements of transient water depth at various locations in these tests.</p>
<p>Nominal spreading angle of 90 degrees assumed for melt exiting pedestal doorway and spreading between pedestal doorway and liner.</p>	<p>Theoretical analysis by Moody predicts spreading angle of 106 degrees for melt approaching doorway with negligible inertia. In general, spreading angle should decrease as velocity of melt approaching doorway increases or depth upstream of doorway decreases. Spreading angles as low as 55 degrees observed in water spreading experiments of Theofanous et. al. conducted in 1:10 scale Mark I geometry. A MELTSPREAD-1 parameter variation was performed assuming a spreading angle of 55 degrees.</p>
<p>Heat transfer downward into the substrate and immobilized solid material is modeled with local, one-dimensional, transient finite difference calculations. Multidimensional heat transfer effects are neglected inside the substrate and immobilized material.</p>	<p>Sideward erosion of sumps or cavities eroded into the substrate is excluded by this assumption which is expected to be good when a limited amount of substrate erosion occurs. As a consequence of the omission of sideward erosion, downward erosion may be overestimated, although the eroded volume is probably underestimated. This would underestimate melt retention in an enlarged sump volume.</p>

**Table 4.1. Summary of Major Assumptions Incorporated into
MELTSREAD-1 (Cont'd.)**

Assumption	Evaluation
<p>Transient, one-dimensional heat conduction is calculated below the concrete erosion front which is assumed to correspond to the concrete liquidus temperature. Release of free water, bound water, and carbon dioxide are assumed to occur at five distinct temperatures below the concrete solidus (Figure 1.1).</p>	<p>This is expected to provide an adequate treatment of local transient gas release from the concrete. The onset of gas release lags in time behind the passage of the leading edge of the spreading melt and is followed by high transient gas fluxes.</p>
<p>The melt velocity and temperature are assumed uniform over the one-dimensional cross section of the spreading melt layer.</p>	<p>For laminar creeping flows, a laminar profile will exist across the layer thickness. Frictional and heat transfer terms are formulated in terms of mean layer velocity and temperature such that actual axial profile is not required. Due to multidimensional effects, local thickness could vary somewhat across lateral extent of melt, but spreading in lateral directions will tend to reduce such variations.</p>
<p>Interfacial friction between the spreading melt and the substrate is modeled with standard engineering correlations for laminar and turbulent open channel flow over a surface characterized by an equivalent sand roughness. The laminar-to-turbulent transition is assumed to occur at a Reynolds number of 2300, while open channel flow data show a transitional range of 500 to 2000. A sand roughness of zero is assumed in the present analysis.</p>	<p>The treatment of interfacial friction is expected to be adequate for the present analysis where spreading is limited much more by melt freezing than by frictional effects. A lower transitional Reynolds number could either increase or decrease the heat losses from the melt depending upon the melt Prandtl number which determines whether the turbulent or laminar forced convection heat transfer coefficient is larger.</p>

**Table 4.1. Summary of Major Assumptions Incorporated into
MELTSPREAD-1 (Cont'd.)**

Assumption	Evaluation
<p>Downward melt-to-substrate heat transfer coefficient assumed to be superposition of Bradley bubbling natural convection coefficient and laminar/Dittus-Boelter forced convection coefficients dependent upon whether local Reynolds number exceeds critical value for onset of turbulence. The forced convection heat transfer coefficient neglects the thermal resistance effects of concrete slag at the melt-concrete interface.</p>	<p>Bradley has noted that the leading coefficient in his correlation should be set to 0.29 rather than 0.53 as assumed in MELTSPREAD-1. He also noted that a recent independent analysis over a wider range of oxide compositions and temperatures yielded leading coefficients somewhat above 0.29. Slag thermal resistance effects will decrease the forced convection heat flux. A MELTSPREAD-1 parameter variation was performed with the downward heat transfer coefficient reduced by a factor of 0.50 together with the upward heat transfer coefficient.</p>
<p>Upward melt-to-water heat transfer encompasses the full boiling curve through film, transition, and nucleate boiling. The film boiling heat transfer coefficient includes effects of thermal radiation, conduction, and coolant subcooling. The upper surface area over which film boiling occurs is assumed to be increased by gas bubbles passing through the surface.</p>	<p>Melt viscosity or non-Newtonian flow effects could retard the passage of gas through the upper surface thereby reducing the area enhancement effects. This would decrease the upward heat losses resulting in greater spreading extents than are calculated.</p>

**Table 4.1. Summary of Major Assumptions Incorporated into
MELTSPREAD-1 (Cont'd.)**

Assumption	Evaluation
<p>Upward melt-to-water and melt-to-atmosphere heat transfer coefficients neglect crust formation at the upper surface. The melt layer bulk temperature is assumed in calculating heat transfer at the upper surface.</p>	<p>Crust formation will lower the upper surface temperature at which energy radiates from the spreading layer. Crusts will also impede the passage of gas through the surface decreasing the effects of gas sparging-induced area enhancement further reducing upward heat losses. This will result in greater spreading extents than calculated. A MELTSPREAD-1 parameter variation was performed with the melt-to-water heat transfer coefficient multiplied by a factor of 0.50 (together with the melt-to-substrate heat transfer coefficient). A second variation involved multiplication of the nominal melt-to-water heat transfer coefficient by a factor of 0.25 with the downward coefficient multiplied by 0.50.</p>
<p>When water is absent, radiative heat transfer from the spreading material to the liner and other overlying structures is neglected.</p>	<p>This assumption is irrelevant to the present analysis in which the drywell is always wet.</p>
<p>Immobilized solid material is nodal-ized in the axial direction and not permitted to commence further relocation until the node temperature exceeds the solidus and there remain no overlying solid nodes.</p>	<p>This assumption affects the continued relocation of solidified material that subsequently heats up and remelts. A molten mass that forms inside a mound of immobilized material is not permitted to break out as the result of localized failure of the constraining "crucible" boundary. This assumption has no impact upon the calculation of depths next to the liner in the present analyses.</p>

**Table 4.1. Summary of Major Assumptions Incorporated into
MELTSPREAD-1 (Cont'd.)**

Assumption	Evaluation
Chemical reaction rates are assumed limited by rate of gas release from concrete. Zr, Cr, and Fe oxidize in sequence.	Slower rates would be expected to result in slightly lesser spreading extents/greater depths than calculated.
Condensed phase chemical reactions involving oxides from the concrete and the released melt such as that between silica and zirconium are ignored.	At temperatures below 2000 to 2200 K, reaction of silica with zirconium can result in an increase in the chemical energy release and decrease in the gas flux from the melt layer upper surface. This would tend to increase the spreading extent. For temperatures above 2000 to 2200 K, opposite trends (i.e., less energy release, greater gas fluxes at the upper surface) can be the result. Parameter variations were performed with greater chemical energy releases intended to bound the effects of condensed phase reactions.
Newtonian melt flow is assumed. The Ishii-Zuber effective viscosity enhancement is assumed for the flow of slurries.	Although completely molten core materials are expected to flow as Newtonian fluids, binary metal alloy mixtures and core-concrete mixtures exhibit non-Newtonian behavior below the liquidus. In this slurry regime, the melt can be described with an effective viscosity, but this viscosity is shear rate dependent. Due to non-Newtonian effects, melt undergoing freezing could exhibit a greater resistance to flow than assumed in analysis resulting in lesser spreading extents/greater depths than calculated.

**Table 4.1. Summary of Major Assumptions Incorporated into
MELTSREAD-1 (Cont'd.)**

Assumption	Evaluation
Solid volume fraction of each phase is assumed to increase continuously between liquidus and solidus.	Non-linear relationships between solid fraction and temperature describe binary metal alloys under non-equilibrium conditions. The impact of this assumption upon the present analysis is probably minor compared with non-Newtonian slurry effects.
Non-Newtonian flow effects upon heat transfer from the spreading melt and the interactions of gases passing through the melt are ignored.	Non-Newtonian behavior is expected to affect heat losses from the melt as well as the dynamics of gas bubbles inside the melt. While a reduction in heat losses relative to those assumed in the present analysis tending to greater spreading extents/lesser depths is the initial expectation, this aspect of non-Newtonian behavior is currently regarded as an area of uncertainty.
Liquidus and solidus temperatures of oxide and metal phases are assumed to remain constant throughout calculation	Actual liquidus and solidus temperatures are composition dependent and will vary due to the addition of concrete erosion products into the melt and chemical reactions. Addition of concrete constituents will decrease the liquidus and solidus temperatures tending to increase the local melt superheat above the liquidus. This will result in greater spreading extents than calculated.

**Table 4.1. Summary of Major Assumptions Incorporated into
MELTSREAD-1 (Cont'd.)**

Assumption	Evaluation
<p>Scenario I substoichiometric U-Zr-O melts are assumed to freeze over a broad temperature range with a linear relationship between specific enthalpy and temperature over the freezing range.</p>	<p>Examination of U-Zr-O quasibinary equilibrium phase behavior reveals that the melt consists of immiscible mainly oxidic and mainly metallic phases. The mainly oxidic phase freezes at a temperature of about 2400 C and the mainly metallic phase freezes at about 1900 C. The presence of other materials in the melt will give rise to the formation of eutectics such that uncertainty must be attached to the actual freezing ranges. Freezing of oxide over a narrower range at 2400 C will tend to sustain higher temperatures resulting in increased heat losses. This is expected to result in lesser spreading extents/greater depths than calculated.</p>
<p>Calculation of crust formation upon solid surfaces neglects decay heat generation inside crusts.</p>	<p>This is not expected to be a significant approximation in the present analysis. It becomes more important as the thickness of the crust increases.</p>

**Table 4.1. Summary of Major Assumptions Incorporated into
MELTSPREAD-1 (Cont'd.)**

Assumption	Evaluation
<p>Mass and enthalpy equations are formulated numerically using an Eulerian finite difference scheme in which convective terms are differenced by mass of the donor cell differencing approach.</p>	<p>The numerical scheme will give rise to so-called "Eulerian mixing" effects which will tend to artificially equilibrate melt depths and temperatures more rapidly than purely physical process. The degree of approximation introduced by Eulerian mixing depends upon the shape of the depth and temperature profiles as well as the numerical grid cell size. Any Eulerian code using donor cell differencing will give rise to this artificial mixing that tends to somewhat reduce the peak calculated melt depths and temperatures. In the calculation by MELTSPREAD-1 of a one-dimensional "dam break" problem for which an analytical solution is available, the time dependent propagation of the advancing and receding fronts was underpredicted by no more than 6 percent.</p>
<p>Momentum equation for spreading melt is formulated numerically using an Eulerian scheme in which the non-conservative form of the momentum equation is differenced.</p>	<p>The non-conservative form is employed such that the melt velocities and depths satisfy Bernoulli's law in the limit of negligible frictional resistance. This is the correct relationship for spreading flows.</p>

2.0 Objectives

The objectives of this work are to directly compare the results of MELTSPREAD-1 code calculations of the corium depth next to the liner with CR1 from NUREG/CR-5423 for consistent conditions of melt release from the reactor vessel.

3.0 MELTSPREAD-1 Input Conditions

The assessment of Theofanous et. al. treats the released melt volume, melt superheat, and metallic zirconium fraction as uncertain quantities characterized by probability density functions (pdfs). In general, the pdfs defined by Theofanous et. al. have the shape of piecewise constant functions. MELTSPREAD-1 input conditions were specified based on combinations of melt volume, superheat, and zirconium content that are possible according to the pdfs as described in this section.

For each of the three scenarios, Tables 4.2 through 4.4 show the ranges of possible volume, superheat, and zirconium fraction corresponding to the values for which the pdfs are nonzero. Also included in Tables 4.2 through 4.4 are the central values of the most probable segments of the pdfs. The central most probable values thus obtained for each scenario were used to define nominal release conditions for the MELTSPREAD-1 calculations. Calculations were also performed using other values within the range of each pdf. Typically, the lower or upper end of the range was used. These calculations serve to determine the extent of variation of the melt depth within the pdf range. Of course, a very low probability is attached to end-of-range release conditions in NUREG/CR-5423.

In order to mechanistically calculate the spreading of melt, additional information concerning the melt release must be specified for each scenario. The rate of release of melt into the pedestal is of particular importance. In quantifying CR1 for each scenario, the release rates shown

in Table 4.5 were assumed by Theofanous et. al. These same release rates were also used as nominal values in the MELTSPREAD-1 calculations. For Scenario I, a calculation was additionally performed for a higher release rate of 6.5 cubic meters per minute equal to the release rate used to scale one of the simulant material spreading experiments documented in NUREG/CR-5423.

The chemical composition of the released melt is also required as input to MELTSPREAD-1. The metallic zirconium content by itself does not provide sufficient information to determine the composition. In specifying the melt composition, it was sought to maintain consistency with NUREG/CR-5423 to the greatest extent possible. Fortunately, such compositions were specified by Theofanous et. al. in the course of carrying out core-concrete interaction calculations used to construct Causal Relation Two providing the superheat duration time for melt next to the liner. Based upon Table 5.5 of NUREG/CR-5423, the melt compositions corresponding to specific zirconium contents shown in Table 4.6 were obtained and used in the MELTSPREAD-1 calculations. It is noted that the compositions in Table 4.6 include the lower end, center of the most probable segment, and upper end compositions associated with the zirconium content probability density function. For each composition, MELTSPREAD-1 requires the specification of liquidus and solidus temperatures. The liquidus and solidus temperatures input to the code were obtained from phase diagrams and have

also been included in Table 4.6.

All calculations were carried out for the geometry of the Peach Bottom 2 and 3 Mark I units to maintain consistency with NUREG/CR-5423 which considered only the Peach Bottom plant. Figure 4.2 shows the nodalization of the spreading channel employed for most of the calculations of the present comparison. It is necessary to define more than one node inside the pedestal to distinguish between the sump which is initially covered with carbon steel plates and the surrounding concrete pedestal floor. The large node in the central region of the pedestal is a representation of the in-pedestal sump region. A single sump node is used in order to uniformly distribute the arriving melt over the full extent of the sump as would be the result of vigorous splashing of the impinging melt stream(s) relocating from the reactor vessel lower head. The sumps can potentially retain a portion of the released melt that would otherwise be available to spread over the surrounding pedestal floor. The concrete composition is taken to be a limestone-common sand variety consistent with the limestone-common sand concrete used in the Peach Bottom units.

In modeling the sump region, assumptions about melt flow and retention inside the sumps were made that are consistent with those made by Theofanous et. al. in their quantification of CR1. In particular, for Scenario I, Theofanous et. al. assumed that 5.75 cubic meters of melt always fills the sump volume. Accordingly, it was assumed in the MELTSPREAD-1 calculations for Scenario I that the sumps constitute an open sunken volume equal to 5.75 cubic meters and that all melt released from the vessel collects inside the

sumps. Thus, melt must first fill up and overflow the sump volume before spreading over the surrounding pedestal concrete floor commences. This assumption is equivalent to assuming that meltthrough of the sump cover plates occurs immediately upon contact with the melt first released from the vessel.

In Scenario II, Theofanous et. al. assumed that the sump covers survive such that the sumps are inaccessible to the debris. In concert with this assumption, it was assumed in MELTSPREAD-1 that the sump volume remains inaccessible throughout the Scenario II calculations. As released melt collects inside the pedestal, it is thus assumed to first spread over the intact steel sump covers and then spread over the surrounding concrete floor.

In Scenario I and the averaged release variant of Scenario II, melt is assumed released at a constant release rate until the total released volume is attained. However, some elaboration is required concerning the peak release variant of Scenario II. As discussed by Theofanous et. al., the peak release here follows an initial slower release phase during which 8 cubic meters is released at a lower rate of ~ 0.15 cubic meter per minute. This characteristic of the release behavior was confirmed through a telephone conversation with Professor Theofanous (Theofanous, July 23, 1992). Therefore, the MELTSPREAD-1 calculations for the peak release case of Scenario II model this sequential release of melt over two phases. Specifically, a volume of 8 cubic meters is first released at a rate of 0.15 cubic meter per minute followed by a variable volume of melt at the peak release rate of 0.70 cubic meter per

minute. It is noted here that pdf1 in NUREG/CR-5423 is formulated in terms of the volume released only at the peak rate rather than the total volume while CR1 is plotted versus the total released volume from both phases.

Several calculations were performed in which the values of certain parameters which relate to the melt spreading dynamics were varied in order to cover current uncertainties in the parameters. The specific parameters are:

- i) the angle of spread of the melt as it exits the doorway leading from the pedestal to the drywell annulus;
- ii) the melt slurry solid fraction below which the melt immobilizes due to a greatly enhanced resistance to flow; and
- iii) the heat transfer coefficients for upward and downward heat losses from the spreading melt.

Table 4.7 shows the nominal values assumed in MELTSPREAD-1 for the parameters and the variations carried out.

The UCSB simulant material spreading experiments documented in NUREG/CR-5423 revealed spreading patterns indicative of spreading angles as large as 90 degrees and as low as 55 degrees as the melt exits the drywell doorway. Accordingly, the spreading angle was varied from the value of 90 degrees nominally assumed in MELTSPREAD-1 to 55 degrees. It is expected that the spreading angle is related to the melt release rate such that an angle of 90 degrees is a very good assumption in the

limit of small release rates. Consequently, a case involving an assumed spreading angle of 55 degrees is thought to be more relevant to Scenario I than Scenario II.

MELTSPREAD-1 nominally assumes that the resistance to flow of a spreading melt slurry increases rapidly as the proportion of solid in the slurry approaches unity. Experiment data suggests that for many systems a large resistance to flow is encountered at solid fractions ranging between 0.4 and 0.7. To account for uncertainties in spreading melt immobilization, a 0.5 solid volume fraction giving rise to a greatly enhanced flow resistance was used as well as the nominal 1.0 value.

The downward melt-to-substrate heat transfer coefficient in MELTSPREAD-1 consists of the sum of a coefficient accounting for forced convection horizontal flow and a bubble agitation natural convection coefficient. The forced convection horizontal flow heat flux does not nominally account for the effects of a film of melted concrete slag between the spreading melt and remaining underlying solid concrete. Instead, the slag is assumed immediately entrained by the spreading flow. In contrast, the bubble agitation coefficient does incorporate the slag resistance. The effects of a slag film during spreading are currently uncertain. For non-spreading molten core concrete interactions, the thermal resistance posed by the slag film has been estimated to reduce the downward heat flux by about 50 percent (Bradley, 1988). To cover the potential uncertainties associated with slag thermal resistance effects, the downward heat transfer coefficient was varied through a 50 percent reduction.

MELTSPREAD-1 calculates upward heat transfer from the spreading melt layer using the bulk melt layer temperature. A distinct upper surface temperature is not calculated. As melt spreads, the heat transfer phenomena at the upper surface typically involve film boiling. At the temperatures characteristic of Scenario I, thermal radiation effects from the melt upper surface to the water predominate. If crust segments form at the melt upper surface and substantially cover the surface, then the appropriate temperature for radiation would be the temperature at the top of the crust segments. If the segments are thin as they would be if sparging gases cause the crusts to break up and to be entrained into the bulk layer, then this temperature would be expected to approximately equal the crust freezing temperature. An estimate of the range over which radiant film boiling heat transfer might be reduced due to crusting is thus given by the ratio,

$$\left(\frac{T_{\text{solidus}}}{T_{\text{bulk}}} \right)^4.$$

In Scenario I, the bulk temperature initially exceeds the oxide liquidus temperature of 2670 degrees Kelvin by a most probable range with a central value of 37 degrees Kelvin. Using this value for the bulk temperature together with an oxide solidus temperature of 2170 degrees Kelvin, a fourth power temperature ratio of 0.4 is calculated. Thus, the upward heat flux might be 60 percent lower due to the formation of thin crust segments. For those melt compositions with a solidus temperature of 1970 degrees Kelvin, a reduction ratio of 0.3 is obtained. In the present analysis, parameter variations are performed for both Scenarios I and II with the upward melt-to-water heat transfer coefficient reduced by 50 percent together

with the downward heat transfer coefficient. For Scenario I, an additional variation is also carried out with the upward heat transfer coefficient reduced by 75 percent.

In formulating Casual Relation 2 characterizing the melt superheat duration next to the liner, Theofanous et. al. conservatively limited upward heat losses from the melt to coolant to upper limits of 1.5 and 1.0 Megawatts per square meter for Scenarios I and II, respectively. The upward heat losses nominally calculated with MELTSPREAD-1 for Scenario I often exceed these values. As noted above, an additional Scenario I parameter variation was also performed in which the upward heat transfer coefficient in MELTSPREAD-1 was reduced by 75 percent and the downward heat transfer coefficient coincidentally reduced by 50 percent. This variation results in upward heat fluxes bounded by the limits considered by Theofanous et. al. and thus serves to determine the effects of the conservative upward heat loss assumptions made in NUREG/CR-5423 upon the corium depth adjacent to the liner.

Parameter variations were performed in which the chemical energy release within the layer was increased to encompass the energetics effects of condensed phase chemical reactions. MELTSPREAD-1 nominally calculates the successive oxidation of zirconium, chromium, and iron by water vapor and carbon dioxide liberated through heatup and decomposition of the concrete substrate. For example, the zirconium oxidation reactions are

The oxidation rate is assumed limited by the gas release rate from the concrete. At temperatures below about 2000 to 2200 degrees Kelvin, silica in the concrete slag formed by concrete erosion can also interact with zirconium giving rise to additional energy release. In particular, the reaction,

can produce metallic silicon. For the present analysis, this and other associated reactions that could increase the energy release are considered. The silicon can react with carbon monoxide to form silicon carbide and carbon dioxide through the reaction,

The CO_2 formed may further react with zirconium through Equation 4-2 that is rewritten here as

From the viewpoint of energetics, the net effect of Equations 4-3 through 4-5 may be represented by

The limestone aggregate-common sand concrete composition assumed in the calculations is shown in Table 4.8. For the relative proportions of silica and carbon dioxide corresponding to this

composition, the reactions represented by Equation 4 through 6 are limited by the rate of silica addition to the melt layer. The fractional increase in the energy release relative to that calculated solely with Equations 1 and 2 is given by

$$\frac{w_{\text{SiO}_2} 1.2 \text{ MJ/Kg}}{w_{\text{H}_2\text{O}} \frac{M_{\text{Zr}}}{2M_{\text{H}_2\text{O}}} 6.7 \text{ MJ/Kg} + w_{\text{CO}_2} \frac{M_{\text{Zr}}}{2M_{\text{CO}_2}} 5.8 \text{ MJ/Kg}} \quad (4-7)$$

where

$$w_{\text{SiO}_2} = \text{mass fraction of silica in concrete,}$$

$$= 0.288,$$

$$w_{\text{H}_2\text{O}} = \text{mass fraction of free and bound water in concrete,}$$

$$= 0.062,$$

$$w_{\text{CO}_2} = \text{mass fraction of carbon dioxide in concrete,}$$

$$= 0.218,$$

$$M_{\text{Zr}} = \text{molar weight of zirconium,}$$

$$= 91.224,$$

$$M_{\text{H}_2\text{O}} = \text{molar weight of water vapor,}$$

$$= 18.0152,$$

$$M_{\text{CO}_2} = \text{molar weight of carbon dioxide,}$$

$$= 44.0098.$$

This ratio is presently evaluated to have a value of 1.4. Thus, the condensed phase reactions might increase the energy release by as much as 140 percent. In view of this, the energetics effects of condensed phase reactions was investigated by increasing the energy release

from zirconium-gas phase reactions (Equations 4-1 and 4-2) by a factor of 2.4. This enhancement to the chemical energy release is expected to constitute an upper bound to the possible increase associated with condensed phase reactions.

Equation 4-2 creates one mole of carbon monoxide for each mole of carbon dioxide liberated from the concrete. The net effect of Equation 4-6 is to destroy one mole of carbon monoxide for each mole of silicon carbide formed. This will reduce the molar flux of gas leaving the melt layer upper surface relative to that entering at the bottom surface. For this reason, a Scenario I case involving an enhanced energy release was also calculated with the melt-to-water heat transfer coefficient reduced. A factor of

two reduction together with a coincident reduction in the melt-to-substrate heat transfer coefficient were assumed so that the results could be compared with those obtained from a case involving the parameter variations in the heat transfer coefficients by themselves.

In all cases, drywell was assumed to be flooded with water in all calculations consistent with the quantification of CR1 by Theofanous et. al. The drywell conditions shown in Table 4.9 were assumed in the present comparison. The drywell pressure was taken to be 0.3 Megapascal based upon the results of a BWR SAR calculation for a short-term station blackout sequence in Peach Bottom (Hodge, 1991).

Table 4.2. Range of Melt Release Conditions for Scenario I Obtained from Probability Density Functions of NUREG/CR-5423

Melt Release Condition	Low End of Range	Center of Most Probable Segment	Top End of Range
Released Melt Volume, m ³	7	12	17
Released Melt Superheat, K	0	37	150
Zirconium Content, wt %	0	15	40

Table 4.3. Range of Melt Release Conditions for Scenario II Peak Release Obtained from Probability Density Functions of NUREG/CR-5423

Melt Release Condition	Lower End of Range	Center of Most Probable Segment	Top End of Range
Released Melt Volume ^(a) , m ³	7	10	15
Released Melt Superheat, K	50	87	150
Zirconium Content, wt %	10	25	50

a) Incremental volume released only at the peak release rate.

Table 4.4. Range of Melt Release Conditions for Scenario II Averaged Release Obtained from Probability Density Functions of NUREG/CR-5423

Melt Release Condition	Lower End of Range	Center of Most Probable Segment	Top End of Range
Released Melt Volume, m ³	16	30	32
Released Melt Superheat, K	50	87	150
Zirconium Content, wt %	10	25	50

Table 4.5. Melt Release Rates Obtained from NUREG/CR-5423 and Assumed in MELTSPREAD-1 Calculations

Release Scenario	Release Rate, m ³ /min
Scenario I	4.0
Scenario II Peak Release	0.7
Scenario II Averaged Release	0.15

Table 4.6 Released Melt Composition Corresponding to Zirconium Fraction Obtained from NUREG/CR-5423 and Freezing Range Assumed in MELTSPREAD-1 Calculations

Scenario	Zirconium Content, wt %	Composition wt % UO ₂ /Zr/ZrO ₂ /Fe/Cr /Ni	Solidus Temperature, K	Liquidus Temperature, K
Scenario I	0	67/0/33/0/0/0	2822	2835
Scenario I	15	70/15/15/0/0/0	2170	2670
Scenario I	30	70/30/0/0/0/0	1970	2670
Scenario I	40	60/40/0/0/0/0	1970	2670
Scenario II	10	18/10/0/57/10/5	1580	1670
Scenario II	30	14/30/0/45/7/4	1830	1930
Scenario II	50	24/50/0/21/3/2	1370	1670

**Figure 4.2. Nodalization of Spreading Channel Assumed in MELTSPREAD-1
Calculations for Case of Ninety Degree Spreading Angle Including
Assigned Node Numbers.**

**Table 4.7. Variations in Melt Spreading Dynamics Parameters Included
in Present Comparison**

Parameter	Nominal Value	Variation
Spread angle	90 degrees	55 degrees
Immobilization solid fraction	1.0	0.5
Upward melt-to-water heat transfer coefficient	---	50 %, 75 % reductions
Downward melt-to-substrate heat transfer coefficient	---	50 % reduction
Chemical reaction energy release from zirconium reactions with concrete decomposition/erosion products	---	140 % increase

**Table 4.8. Limestone Aggregate-Common Sand Concrete
Composition Assumed in MELTSPREAD-1 Calculations**

Constituent	Weight Percent	Mole Percent
SiO ₂	28.8	22.8
MgO	9.8	11.6
CaO	26.4	22.4
Fe ₂ O ₃	1.6	0.5
Al ₂ O ₃	3.6	1.7
Na ₂ O	1.1	0.8
K ₂ O	0.6	0.3
TiO ₂	0.1	0.1
CO ₂	21.8	23.4
H ₂ O	6.2	16.4

Table 4.9. Drywell Conditions Assumed in MELTSPREAD-1 Calculations

Condition	Value
Pressure, MPa	0.3
Water Depth Above Drywell Floor, m	0.70
Water Temperature, K	357
Saturation Temperature, K	407
Water Subcooling, K	50
Initial Structure Temperature, K	407

4.0 Results

Tables 4.10 through 4.12 summarize the various calculations performed for each of the release scenarios and the calculated peak corium depths next to the liner. The melt depth next to the liner calculated with MELTSPREAD-1 is dependent upon time as well as location along the liner. For example, for Case I1, Figures 4.3 through 4.6 present the time dependent melt depths at those nodes in which melt flows in contact with the liner. This is the Scenario I case calculated using release conditions that are equal to those at the centers of the most probable pdf segments. The nodalization scheme is shown in Figure 4.2. As shown by the figures, the depth typically increases with time to a peak value and then decreases somewhat as melt continues to spread in the drywell annulus. It is to be noted that because MELTSPREAD-1 calculates the spreading of melt over previously immobilized corium, the material next to the liner is usually predicted to consist of spreading melt overlying a previously solidified layer. The upper surface elevations of both the immobilized material and the melt flowing over it are shown in each of the figures. It is also important to note that the actual melt height includes the effects of local level swell due to the void fraction through the layer resulting from concrete decomposition gases rising up through the melt. This effect has been indicated in the figure by plotting both the actual melt upper surface elevation as well as the collapsed melt surface elevation that would correspond to removal of the void.

In directly comparing with CR1, the maximum corium depth equal to the peak

combined depth of the spreading melt including level swell and the underlying immobilized corium has been used. For Case I1, it is observed that the peak depth is equal to 10.9 centimeters and that this same peak depth is calculated at all four nodes where melt collects next to the liner. It is also seen that the depth of spreading molten material is greatest at the location directly opposite the pedestal doorway and falls off at greater distances along the liner. The peak spreading melt depth opposite the doorway is 9.6 centimeters. For completeness, Tables 4.13 through 4.15 give the peak corium depth next to the liner, the peak spreading melt depth next to the liner, the peak collapsed corium depth, and the peak collapsed spreading melt depth calculated for each of the cases. It is noted that, in general, the four entries for each case in Tables 4.13 through 4.15 can occur at different locations as well as different times.

Figures 4.7 through 4.9 show the peak depths for the various cases calculated for each release scenario plotted together with CR1. The cases are enumerated in Tables 4.10 through 4.12 which also include the calculated peak corium depth. In the approach of Theofanous et. al., the thermal loading on the liner rises as the corium depth increases. Thus when the calculated depth is less than the band of values specified by CR1, it may be said that the CR1 used in NUREG/CR-5423 conservatively bounds the depth. For Scenario I (Figure 4.7), the peak corium depths calculated with MELTSPREAD-1 lie within the 5 and 95 percentile bounds of CR1 or are bounded by CR1 except in

the vicinity of a released melt volume of 9 cubic meters. For the specific calculation that assumes values for the released melt volume (12 cubic meters), melt superheat (37 degrees Kelvin), and zirconium content (15 weight percent) corresponding to the center of the most probable segments of the respective pdfs (Case I1), the calculated peak depth is seen to be located between the 5 and 95 percent bounds. Holding the release superheat and zirconium content constant at these values and increasing the volume over the possible range to 17 cubic meters provides depths that remain between the bounds. Decreasing the volume to 9 cubic meters results in a depth that just slightly exceeds the 95 percent bound. However, this is observed not to be a significant variance from CR1. Further reduction of the released volume to the lowest possible value of 7 cubic meters provides a depth well below the 5 percent curve. For a fixed melt volume of 9 cubic meters, variations in the melt superheat (reduction from 37 to 12 degrees Kelvin), zirconium content (increase from 15 to 30 percent), and spreading angle (reduction from 90 to 55 degrees) do not significantly alter the calculated peak corium depth. The particularly small effect of varying the melt superheat above the liquidus temperature reflects the modeling in MELTSPREAD-1 that nominally immobilizes the melt when the temperature has decreased down to the solidus. Consequently, the liquidus-to-solidus temperature range represents the greatest portion of the melt energy that must be removed to bring about immobilization.

Comparison with CR1 for the peak release variant of Scenario II is shown in Figure 4.8. For a released melt volume

(18 cubic meters), superheat (87 degrees Kelvin), and zirconium content (30 weight percent) equal to those at the center of the most probable pdf segments, the spreading melt is calculated to immobilize and freeze without ever having spread to the liner. Increasing the released volume to 21 cubic meters does not change this result; a zero depth is still calculated. For fixed values of the superheat, zirconium content, and spreading parameters, the calculation of a zero melt depth at a particular volume implies that the depth is also zero for all lesser released volumes. Thus, a peak depth of zero is also predicted for all volumes less than or equal to 21 cubic meters. Increasing the zirconium content to the maximum possible value of 50 weight percent does not result in melt spreading to the liner for released total volumes as large as 21 cubic meters. In general, significant spatial nonuniformities are calculated in the corium depth profiles for Scenario II. Consequently, it is not straightforward to draw a simple correspondence between a particular parameter variation and the resulting change in the calculated maximum spreading extent. The Scenario II results also exhibit abrupt changes in the depth next to the liner such as the difference in depth between Cases IIA3 and IIA4. This reflects melt freezing as a layer short of the liner in some cases (e.g., Case IIA4) such that a large increase in depth is obtained when the extent of spreading increases to encompass those nodes adjacent to the liner (e.g., Case IIA3).

For the averaged released variant of Scenario II, melt immobilization and freezing without reaching the liner are also calculated for release conditions equal to those at the center of the most

probable pdf segments (Figure 4.9). Increasing the superheat to the highest possible value of 150 degrees Kelvin (Case IIA2) does not change this result. As discussed above, an implication of a calculated peak depth of zero is that the depth is zero for all lesser released volumes as well. Raising the zirconium content to the highest possible value of 50 weight percent (Case IIA3) does result in melt spreading to the liner at a volume of 30 cubic meters. In this case, though, the peak melt depth of 22 centimeters remains below the 5 to 95 percent range given by CR1. For the maximum possible zirconium content, reducing the released volume from 30 to 26 cubic meters which is equal to the center of the next most probable segment of pdf1 results in the calculation of freezing short of the liner. Again, the depth next to the liner will also be zero for all released volumes below 26 cubic meters at the maximum possible zirconium content. A calculation was performed in which the downward and upward heat transfer coefficients were reduced by 50 percent, the superheat was taken to be 100 degrees Kelvin corresponding to the upper end of the most probable segment, and the zirconium content was assumed equal to 50 percent. A peak corium depth next to the liner of 22 centimeters was calculated. Again, this

depth is conservatively bounded by CR1. The lack of spreading to the liner calculated for the peak release variant of Scenario II is consistent with the lack of spreading to the liner obtained for the averaged release variant under similar conditions of zirconium content and superheat. This result would be expected if the melt spreading behavior is not strongly sensitive to an increase in the release rate from 0.15 to 0.70 cubic meter per minute.

For Cases I1, IIP1, and IIA1, the calculated upward melt-to-coolant heat fluxes were also compared with those conservatively assumed in the evaluation of CR2 by Theofanous et. al. in NUREG/CR-5423. For Scenario I (Case I1), the upward heat fluxes range from 1.5 to 4.1 Megawatts per square meter. In Scenario I, Theofanous et. al. assumed a limitation of 1.5 Megawatts per square meter. The parameter variation encompassed by Case I19 results in heat fluxes that vary between 1.0 and 0.3 Megawatts per square meter. For Scenario II (Cases IIP1 and IIA1), the calculated melt-to-coolant heat fluxes range between 0.6 and 0.7 Megawatts per square meter. These values are less than the 1.0 Megawatt per square meter limitation assumed by Theofanous et. al.

Table 4.10. MELTSPREAD-1 Calculations for Scenario I

Case Designation	Released Melt Volume, m ³	Release Melt Superheat, K	Zirconium Content, wt %	Melt Composition, wt % UO ₂ /Zr/ZrO ₂	Other Variations	Spreading Extent, Node No.	Maximum Corium Depth Next to Liner, cm
I1	12	37	15	70/15/15		18	10.9
I2	7	37	15	70/15/15		15	0.4
I3	9	37	15	70/15/15		16	9.4
I4	15	37	15	70/15/15		18	15.5
I5	17	37	15	70/15/15		18	19.5
I6	12	37	15	70/15/15	Reduce spreading angle from 90 to 55 degrees	18	11.4
I7	12	37	15	70/15/15	Decrease immobilization solid fraction from 1.0 to 0.5	2	0
I8	12	150	15	70/15/15	Reduce melt-to-substrate and melt-to-water heat transfer coefficients by 50 percent	22	9.7
I9	12	37	15	70/15/15	Increase release rate from 4.0 to 6.5 m ³ /min	19	10.4
I10	12	37	30	70/30/0		19	10.1
I11	12	37	40	60/40/0		16	9.8
I12	12	37	0	67/0/30		11	0
I13	9	12	15	70/15/15		16	9.2
I14	9	37	30	70/30/0		17	7.2
I15	9	37	15	70/15/15	Reduce spreading angle from 90 to 55 degrees	17	9.6
I16	15	12	15	70/15/15		18	15.4
I17	12	37	15	70/15/15	Increase chemical energy release from zirconium reactions by factor of 2.4	19	10.3
I18	12	150	15	70/15/15	Reduce melt-to-substrate and melt-to-water heat transfer coefficient by 50 percent as well as increase chemical energy release from zirconium reactions by factor of 2.4	22	10.4
I19	12	37	15	70/15/15	Reduce melt-to-water heat transfer coefficient by 75 percent and melt-to-substrate heat transfer coefficient by 50 percent	22	11.0

Table 4.11. MELTSPREAD-1 Calculations for Scenario II "Peak Release"

Case Designation	Released Melt Volume During Peak Release, m ³	Total Released Melt Volume, m ³	Released Melt Superheat, K	Zirconium Content, wt %	Melt Composition, wt % UO ₂ /Zr/ZrO ₂ /Fe/Cr/Ni	Other Variations	Spreading Extent, Node No.	Maximum Corium Depth Next to Liner, cm
IIP1	10	18	87	30	14/30/0/45/7/4		12	0
IIP2	13	21	87	30	14/30/0/45/7/4		12	0
IIP3	10	18	100	30	14/30/0/45/7/4	Reduce melt-to-substrate and melt-to-water heat transfer coefficients by 50 percent	16	0.6
IIP4	10	18	87	50	24/50/0/21/3/2		5	0
IIP5	13	21	100	50	24/50/0/21/3/2		11	0
IIP6	13	21	87	10	18/10/0/57/10/5		5	0
IIP7	10	18	87	30	14/30/0/45/7/4	Increase chemical energy release from zirconium reactions by factor of 2.4	5	0

Table 4.12. MELTSPREAD-1 Calculations for Scenario II "Averaged Release"

Case Designation	Released Melt Volume, m ³	Released Melt Superheat, K	Zirconium Content, wt %	Melt Composition, wt % UO ₂ /Zr/ZrO ₂ /Fe/Cr/Ni	Other Variations	Spreading Extent, Node No.	Maximum Corium Depth Next to Liner, cm
IIA1	30	87	30	14/30/0/45/7/4		8	0
IIA2	30	150	30	14/30/0/45/7/4	Reduce melt-to-substrate and melt-to-water heat transfer coefficients by 50 percent	5	0
IIA3	30	87	50	24/50/0/21/3/2		16	22.2
IIA4	26	87	50	24/50/0/21/3/2		13	0
IIA5	30	100	50	24/50/0/21/3/2	Reduce melt-to-substrate and melt-to-water heat transfer coefficients by 50 percent	17	21.9
IIA6	30	87	30	14/30/0/45/7/4	Increase chemical energy release from zirconium reactions by factor of 2.4	15	0

Figure 4.3. Calculated Corium Height Next to Liner for Scenario I Case I1 at Node 15 Directly Facing the Doorway (Refer to Figure 4.2).

Figure 4.4. Calculated Corium Height Next to Liner for Scenario I Case I1 at Node 16
 (Refer to Figure 4.2).

Figure 4.5. Calculated Corium Height Next to Liner for Scenario I Case II at Node 17
(Refer to Figure 4.2).

Figure 4.6. Calculated Corium Height Next to Liner for Scenario I Case I1 at Node 18
(Refer to Figure 4.2).

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Table 4.13. Peak Corium Depths Next to Liner Calculated by MELTSPREAD-1 For Scenario I

Case Designation	Maximum Corium Depth Next to Liner, cm	Maximum Spreading Melt Depth Next to Liner, cm	Maximum Collapsed Corium Depth Next to Liner, cm	Maximum Collapsed Spreading Melt Depth Next to Liner, cm
I1	10.9	9.6	10.9	8.2
I2	0.4	0.4	0.4	0.4
I3	9.4	8.1	9.3	7.1
I4	15.5	14.2	15.5	12.9
I5	19.5	17.5	19.4	16.0
I6	11.4	10.4	10.9	9.6
I7	0	0	0	0
I8	9.7	9.7	8.4	8.4
I9	10.4	9.6	10.3	8.2
I10	10.1	9.0	10.0	8.2
I11	9.8	9.3	9.5	8.6
I12	0	0	0	0
I13	9.2	8.0	9.2	7.1
I14	7.2	6.3	7.2	5.6
I15	9.6	8.7	9.3	7.6
I16	15.4	14.1	14.9	12.7
I17	10.3	9.3	9.6	8.1
I18	10.4	10.4	9.3	9.3
I19	11.0	11.0	9.8	9.8

Table 4.14. Peak Corium Depths Next to Liner Calculated by MELTSPREAD-1 for Scenario II Peak Release

Case Designation	Maximum Corium Depth Next to Liner, cm	Maximum Spreading Melt Depth Next to Liner, cm	Maximum Collapsed Corium Depth Next to Liner, cm	Maximum Collapsed Spreading Melt Depth Next to Liner, cm
IIP1	0	0	0	0
IIP2	0	0	0	0
IIP3	0.6	0.6	0.6	0.6
IIP4	0	0	0	0
IIP5	0	0	0	0
IIP6	0	0	0	0
IIP7	0	0	0	0

Table 4.15. Peak Corium Depths Next to Liner Calculated by MELTSPREAD-1 for Scenario II Averaged Release

Case Designation	Maximum Corium Depth Next to Liner, cm	Maximum Spreading Melt Depth Next to Liner, cm	Maximum Collapsed Corium Depth Next to Liner, cm	Maximum Collapsed Spreading Melt Depth Next to Liner, cm
IIA1	0	0	0	0
IIA2	0	0	0	0
IIA3	22.2	4.6	22.1	4.5
IIA4	0	0	0	0
IIA5	21.9	16.4	21.9	16.1
IIA6	0	0	0	0

Figure 4.7. Comparison of Peak Corium Depth Next to Liner Calculated with MELTSPREAD-1 versus CR1 for Scenario I.

Figure 4.8. Comparison of Peak Corium Depth Next to Liner Calculated with MELTSPREAD-1 versus CR1 for Scenario II Peak Release.

Figure 4.9. Comparison of Peak Corium Depth Next to Liner Calculated with MELTSPREAD-1 versus CR1 for Scenario II Averaged Release.

5.0 Summary of Findings

The MELTSPREAD-1 computer code has been used to calculate the corium depth next to the liner for melt release conditions consistent with those of Theofanous et. al. in NUREG/CR-5423.

For Scenario I, virtually all of the calculated peak corium depths lie within the 5 and 95 percentile bounds of CR1 or are conservatively bounded by CR1. Calculated peak corium depths exceed the 95 percentile bound of CR1 only for a released melt volume of 9 cubic meters. However, the variance from the 95 percent bound here is very small (see Fig-

ure 4.5) such that the calculated depths do not differ significantly from the depth given by the 95 percentile bound.

For both the peak release and averaged release variants of Scenario II, the calculated peak corium depths next to the liner are conservatively bounded by CR1 (Figures 4.6 and 4.7). In fact, for melt release conditions that correspond to the center of the most probable segments of the probability density functions from NUREG/CR-5423, the melt is calculated to freeze short of contacting the liner such that the peak depth is zero.

6.0 References

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**PART V: CREEP-RUPTURE FAILURE
IN A MARK I DRYWELL LINER**

Abstract

A three-dimensional, finite element, structural analysis of a Mark I drywell liner in localized contact with reactor core debris under water is described. The analysis was done with the ABAQUS computer code and constitutive relationships for steel described in the report. The analysis was undertaken to determine if creep rupture could lead to liner failure at temperatures significantly less than the melting temperature of the linear steel. Failure criteria were developed in terms of creep rupture strains and the accumulated Damage Index based on the Larson-Miller parameter. Because a suitable data base could not be found for the strength properties of the A516 steel used to make the Mark I liners, analyses were done using a data base for another fine-grained ferritic steel, A533. The particular accident scenario involved debris at a maximum depth of 20 cm (angle of repose = 7.9°) in contact with a 22.4° arc of liner. Debris in this configuration has been calculated to heat the liner to a maximum temperature of 1300°C in 10 minutes. Creep rupture is predicted to fail the liner at 1260°C which is about 240°C less than the liner melting temperature. Uncertainty in the Larson-Miller parameter data base is used to define the 95 percent confidence bounds of the failure temperature of the liner for the configuration analyzed to be 1238 to 1310°C .

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Executive Summary

A three-dimensional creep-rupture analysis has been done for a Mark I drywell liner exposed to core debris under water. The analysis, done with ABAQUS computer code, was undertaken to determine if creep rupture induced by localized contact of the liner with core debris could lead to structural failure at temperatures significantly less than the melting temperature of the liner steel. Melting has been the criterion for liner failure used in previous analysis of the Mark I liner including the analyses presented in NUREG-1150 and NUREG/CR-5432. For this limited objective, a single, hypothetical, accident sequence was considered. In this accident, core debris was assumed to contact a 22.4° arc of the liner immediately opposite the pedestal doorway of a Mark I drywell. The maximum depth of the debris, at the midpoint of the arc, was taken to be 20 cm. Analyses in NUREG/CR-5423 indicate that the liner in contact with the debris would be heated to a maximum temperature of 1300°C in 10 minutes. The pressure in the drywell was taken to be 0.2 MPa (29.4 psi).

The localized heating of the liner creates biaxial stress. This biaxial condition is outside the range where conventional Larson-Miller parameter failure analyses are rigorously applicable. Consequently, failure criteria have been developed in terms of strain as well as stress. An accumulated Damage Index or life fraction rule based on the Larson-Miller parameter for the steel has been defined. The first criterion for failure is that this Damage Index exceeds one. This criterion was supplemented with a criterion based on observed creep rupture strains corrected for the biaxial configuration of the liner.

To apply these failure criteria in the analysis of the Mark I liner, it is necessary to have a data base of material properties of liner steel extending to high temperatures. A suitable data base for the A516 steel used for the Mark I liner was not found. Data bases for a somewhat similar fine-grained, ferritic steel, A533, extended to 1100°C were obtained. A516 and A533 steels have rather different properties at low temperatures below about 627°C. Material transformations, especially at temperatures above the ferrite-to-austenite phase transition at 727°C, cause properties of these two steels to become very similar. Since interests were focused on liner behavior at elevated temperatures, the properties of A533 steel are adequate for this work.

Material property data bases for A533 steel were obtained from the Idaho National Engineering Laboratory (INEL) and the Electric Power Research Institute (EPRI). The INEL data provided high-temperature yield strength, creep rates, and the Larson-Miller parameter (LMP) as functions of time, temperature and stress as appropriate. The EPRI data provided yield strength and creep rates at lower temperatures. Both sets of data were analyzed and merged to provide a continuous data base for temperatures to 1100°C. This data base was used in a rate-dependent, elasto-plastic constitutive model capable of tracking the material response throughout its history from the very low-temperature, purely elastic regime to the high temperature, low strength, large strain rate regime. Material properties for temperatures greater than 1100°C were taken to be the same as those derived from the data base for a temperature of 1100°C.

The three-dimensional analysis of liner response during the 10 minute transient heating predicted failure initiation at the inner surface of the shell at 1140°C (2090°F). Complete, throughwall failure was predicted to occur at 1260°C (2300°F). These failure predictions are based on simultaneously satisfying the Damage Index criterion and the creep-rupture strain criterion. It is recognized that there are uncertainties in the quantitative values used in these criteria. Larson-Miller parameters used in the Damage Index are derived by least-squares analyses of experimental data. A quantitative uncertainty analysis of this fitting was done. Scatter in the creep rupture strain data is large. Consequently, the one standard deviation bound on a least-squares regression of these data against temperature was used to

define the criterion. This bound accounts for uncertainties not explicitly taken into account such as thinning of the metal by corrosion or flaws in liner fabrication. Based on the quantitative uncertainty analysis, the 95 percent confidence bounds on the failure temperature for the configuration analyzed are 1238 to 1310°C.

The analysis has been done for a single accident scenario. No attempt has made to assign a probability to this particular scenario. Creep is a function of both time and temperature. Changes in the specification of the rate of heating, peak temperature or extent of liner contact with core debris could affect the prediction of the liner failure temperature.

1.0 Introduction

The vulnerability of the Mark I drywell liner to attack by molten core debris has long been an issue of concern [1-6]. Rupture of the liner by melt attack early in the ex-vessel phases of a reactor accident could lead to large releases of radioactive material from the plant. The recent probabilistic risk assessment of a Mark I boiling water reactor sponsored by the U.S. Nuclear Regulatory Commission established that there is substantial technical uncertainty about the effects water on the drywell floor will have on the vulnerability of the liner to attack by core debris [7]. Resolution of this issue of drywell liner vulnerability is a priority activity in the strategy developed by the U.S. Nuclear Regulatory Commission to resolve severe reactor accident issues [6].

Nearly all past analyses of core debris attack on the drywell liner have been based on liner melting as the failure criterion [1-5]. This appears to be a satisfactory criterion when there is no water present on the drywell floor. There is little doubt that in this case the liner can be heated rapidly to very high temperatures. The melting criterion may not be as satisfactory when a water pool is maintained in the drywell. Cooling of both the core debris and the liner may slow the heating of the liner and even prevent the liner from reaching its melting point ($\sim 1500^{\circ}\text{C}$).

Rupture of the liner in contact with core debris may not be prevented simply because the temperature is kept below the melting point. The liner (SA516-70 carbon steel) rapidly loses strength at temperatures above 427°C . The temperature dependence of carbon steel properties becomes especially severe at temperatures above the ferrite-to-austenite phase transition at 732°C . This is true for all fine-grained ferritic steels. Consider, for example, the SA533-B1 pressure vessel steel. At 316°C its yield strength is about 345 MPa (50 ksi). At 732°C the yield strength is only 69 MPa (10 ksi) and at 1093°C the yield strength is about 14 MPa (2 ksi). At temperatures above about 650°C the creep of the steel, which drives stress relaxation and

redistribution, becomes significant. For example, a creep rate of 1 percent per hour can be supported by a stress of 207 MPa (30 ksi) at 650°C and by a stress of only about 7 MPa (1 ksi) at 1093°C . The creep rate of the steel is quite sensitive to the stress level. At 1093°C increasing the stress from 7 MPa (1 ksi) to 14 MPa (2 ksi) increases the creep rate by about a factor of 30. Thus, creep rupture may be the failure mode of the Mark I boiling water reactor drywell liner when it is simultaneously exposed to both core debris and water. Creep rupture of the liner can occur at temperatures substantially below the melting point of the liner.

This report presents results of a three-dimensional structural analysis of a Mark I drywell liner exposed to core debris and water. The general arrangement of the Mark I boiling water reactor and the configuration of the core debris and liner are shown in Figure 5.1. Many of the previous thermal analyses of the vulnerability of the Mark I liner to core debris attack assumed a two-dimensional axisymmetric configuration based on a belief that core debris would rapidly spread across the entire drywell floor [1-5]. Indeed a two-dimensional axisymmetric structural analysis was done in the first phase of this work and the results of this analysis have been reported elsewhere [8]. Recent mechanistic analyses of the core debris spreading process when water is on the drywell floor (see NUREG/CR6025 Part II) suggest spreading will be inhibited. Because of this finding, analyses of the structural response are done here for core debris contacting only a 22.4° arc of the liner. The objective of the analyses is to define a temperature criterion for liner failure based on creep rupture.

Temperature and pressure boundary conditions employed in the structural analysis are based on a recent, detailed thermal analysis of core debris behavior during a hypothetical severe reactor accident analysis in a Mark I boiling water reactor [1]. The accident considered here corresponds to Scenario I of this thermal analysis.

Figure 5.1. The liner attack configuration in a Mark I drywell

The structural analysis was done using the ABAQUS computer code (version 4.9-1) [9]. ABAQUS is a widely used, general purpose, finite element code. The analysis done here, however, is not routine. Ferritic steel used to make the Mark I drywell liners was never intended for service conditions involving temperatures as high as those expected to occur when core debris contacts the liner even when water is present. To model the behavior of the liner accurately under these conditions, a physically descriptive and rigorously formulated constitutive model is required. The constitutive model must account for the temperature-dependent and strain-rate dependent deformations of the liner. The constitutive model utilized in the analysis reported here is a viscoplasticity model developed from data for a fine-grained, reactor pressure vessel steel. Details of the model are presented in Appendix B to this report. The model is coded in a subroutine for use with the ABAQUS code. This technique of coupling material models with the ABAQUS code has been used previously by Clauss [10] and by Dameron et al. [11] in research sponsored by the U.S. Nuclear Regulatory Commission on concrete containment behavior. The approach has been widely and successfully used in experiment verification work.

The constitutive model requires an extensive base of experimental data. A suitable database for the SA516 carbon steel used to make the liner could not be found. Two sets of data extensive enough to be used in the constitutive model were found for the rather similar SA533-B1 pressure vessel steel. One data set was obtained from the Idaho National Engineering Laboratory (INEL) program researching reactor vessel lower head failure [12]. The second data set is from a program sponsored by the Electric Power Research Institute (EPRI) to study high-temperature properties of reactor vessel steels [13]. These two data sets were analyzed and mathematically fitted to be consistent with the constitutive model. The data and the curve fitting are described in Appendix A of this report. These databases for SA533 steel were thought to be applicable for the analysis of

SA516 steel. The basis for this conclusion is presented in Appendix A. All fine-grained, carbon steels exhibit similar temperature dependencies in their properties at temperatures above 427°C and close similarities at temperatures above the ferrite-to-austenite phase transition at 727°C. Material transformations that occur rapidly at high temperature quickly eliminate many of the differences in the properties of these two steels. Creep damage to the liner was expected to be most extensive at even higher temperatures where there should be a great similarity in material properties of the two steels.

Extensive though the database is, it is not as complete as needed for the analyses done here. It was necessary to assume that the creep properties of the steel above 1100°C were the same as those measured at 1100°C. This probably is a non-conservative approximation. Indeed, it is found that for the three-dimensional analysis described below, rupture is predicted for temperatures greater than 1100°C.

Accurate modeling of the liner behavior also requires that very detailed, finite element computation grids be used to capture the highly localized effects that govern the liner response. Well-posed thermal and mechanical boundary conditions as well as a proper account of the steel liner interactions with the concrete floor and reactor shield wall are also necessary. The concrete constitutive model used for these concrete structures accounts for cracking, plasticity, crushing, and temperature-induced degradation of the concrete stiffness and strength [14].

Finally, it is necessary to have a history-dependent criterion for failure of the deforming liner that can be expressed in terms of computable responses of the liner. A life fraction approach based on the LMP is used here to predict shell rupture as a function of time and temperature. It should be noted, however, that the experimental LMP data that are available have been determined from constant load and constant

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creep experiments. These experimental determinations could not be used directly in a time history analysis for failure prediction. The LMP data were modified for application to time-dependent stress and temperatures. The LMPs were used to compute rupture times for prevailing conditions during a computational step in the finite element analysis:

$$t_r [\sigma(j), T(j)] \\ = \exp \{2.303[(LMP(j)/0.001T(j) - 20)]\}$$

where

$t_r [\sigma(j), T(j)]$ = time to creep rupture as a function of stress and temperature during the j^{th} calculational step,

$LMP(j)$ = applicable LMP for conditions during the j^{th} calculational step,

$\sigma(j)$ = stress during the j^{th} calculational step, and

$T(j)$ = temperature in degrees Rankine during the j^{th} calculational step.

These rupture times were then used to formulate a Damage Index, D , to describe the accumulation of creep damage to the liner during heatup:

$$D = \sum_{j=1}^n \frac{\Delta t(j)}{t_r[\sigma(j), T(j)]}$$

where $\Delta t(j)$ is the time duration considered in the j^{th} calculational step. A Damage Index of 1 was taken to indicate rupture at 50 percent confidence level.

There is, however, some uncertainty associated with the computation of the Damage Index based on LMP data obtained in constant-load creep tests that do not simulate at all the spatially and temporally varying stress and temperature conditions in the liner. Consequently, the Damage Index criterion was augmented by a second criterion. A Damage Index of $D \geq 1$ was taken to indicate rupture if effective plastic strains were in the vicinity of creep rupture strains for the two-dimensional configuration. Data available on the creep rupture strains are from uniaxial tests. They are very scattered as rupture strain data are apt to be. To account for uncertainties in these data, a procedure described in detail in Appendix C was followed. The rupture strain data from uniaxial tests was linearly regressed against temperature. A one standard deviation displacement of this line was adopted as the uniaxial rupture strain. For application to the biaxial configurations of interest here, a biaxial factor of 2 was used. This defines a failure criterion to augment the Damage Index criterion. This strain-based, second criterion was, however, used in a more qualitative way than the stress-based Damage Index criterion. The rupture strain criterion was used to exclude predictions of rupture when strains are small despite large computed values of the Damage Index.

Analyses reported here are interpreted in terms of a temperature at which the Mark I liner will rupture. The intent is to offer an alternative to the melting point failure criterion used in the past which might not be applicable for situations in which water is present in the drywell. The melting point of carbon steel is relatively well-known. There will, on the other hand, be some significant uncertainty that must be ascribed to a failure criterion based on creep rupture. The authors of this report believe the most important sources of the uncertainty are:

- the material model formulated to treat high temperatures and large strains, and
- the failure criteria adopted in this work.

The magnitudes of uncertainties from these sources are difficult to quantify.

Other sources of uncertainty include the material data, the finite element computational model, and the geometric representation of the surface. These uncertainties are somewhat easier to quantify and control. For example, conventional statistical methods can be used to define confidence bounds for materials properties such as creep rate, yield strength, elastic modulus, and even LMP. Uncertainties related to the finite element mesh can be minimized by using a highly refined mesh to the limit that resources permit.

There are also uncertainties associated with the application of these results to a particular reactor.

Analyses are reported here for a generic Mark I liner as designed. There has been no attempt to account for the effects of corrosion of the liner material or flaws in the installation of the liner.

Finally, there are uncertainties that arise from the boundary conditions. The analysis reported here is for a single example of a class of severe reactor accidents represented by Scenario I of Reference 1. Creep rupture is, however, a function of both time and temperature. Changes in the specifications of the rate and extent of heating of the liner could affect the selection of a temperature of failure.

2.0 The Three-Dimensional Analysis

The approach and the results of a three-dimensional analysis of the Mark I liner response to contact with core debris are described below. Formulation of the analysis was assisted by the experience gained in a two-dimensional, axisymmetric analysis of the same problem. Results of this two-dimensional analysis are reported elsewhere [8].

2.1 Finite Element Model

2.1.1 Grid

Because of the excessive computational cost for three-dimensional brick elements, it was necessary to perform the three-dimensional analysis using shell elements for the liner. A schematic representation of the three-dimensional model is shown in Figure 5.2. Eight node shell elements (in ABAQUS, S8R) with reduced 2x2 integration were used for the steel, and twenty node quadratic elements (C3D20R) with reduced 2x2x2 integration were used for the sand. The shield building, concrete floor, and the gap elements, as demonstrated by the previous two-dimensional analysis, do not contribute to the failure mechanism and, therefore, were not included in the three-dimensional model. (In both the previous two-dimensional analysis [8] and in three-dimensional analysis presented here, the radial deformation of the liner does not exceed the gap lengths of 5 cm between the concrete and the steel, and therefore gap elements and the shield building never affect the results because the gap elements do not close.) Note that the penetrations of the liner by downcomers to the suppression pool are not modeled in the three-dimensional analysis. For the particular nuclear plant of interest, the penetrations for the downcomers are more than 30 cm above the drywell floor. This is high enough that the downcomer penetration ought not affect the stress and temperature distributions produced in the liner by contact with the debris. Inspection of the results obtained in this work confirms the adequacy of this assumption.

2.1.2 Boundary Conditions

The boundary conditions for the analysis are shown in Figure 5.2. The model imposes horizontal constraints at the centerline and a stress boundary condition at the equilateral plane that is equal to the membrane stress in a perfect sphere:

$$\sigma_m = Pr/2t$$

where

σ_m = membrane stress,

P = pressure,

r = radius of the sphere, and

t = thickness of the sphere wall.

It is important to apply stress constraints rather than displacement constraints because of the thermal expansion to the liner.

2.2 Applied Pressure and Temperature Conditions

The pressure within the drywell recommended by Professor T. G. Theofanous (private communication) is a constant 0.2 MPa (2 atmosphere) applied in the analysis at the inner surface. This pressure represents the approximate steady-state pressure expected during the severe accident sequence. The lower portion of the primary shell is assumed to be full of water, and this water is in the free-boiling condition (the ambient pressure in the containment is not high enough to support substantially higher water temperatures than 100°C [212°F]). The weight of the water has been considered.

The temperature conditions away from core debris in contact with the shell are the same as the water, 100°C. Temperatures other than standard temperature, 25°C, and pressure,

Figure 5.2. Schematic representation of the three-dimensional grid used for the finite element structural analysis

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0.1 MPa (one atmosphere), conditions are applied only to the shell. This includes the first few elements embedded in concrete (though the concrete is not modeled). The sand is assumed to be largely unaffected by the shell temperatures.

The temperature distribution associated with core debris contact was formulated from recommendations made during a project meeting with the sponsors of the work (February 18, 1992, San Diego, CA) and published in NUREG/CR-5423 [1]. The distribution as input to ABAQUS is plotted as temperature contours in Figures 5.3 and 5.4. In Figure 5.3 the entire grid is shown; in Figure 5.4 the local area of interest is shown. Note that the highest temperature applied is 1300°C (2372°F). The distribution presented in the NUREG/CR-5423 [1] is reproduced here as Figure 5.5. However, instead of applying the distribution as indicated in these two-dimensional contours, the temperatures, shown here through the thickness, were mapped around the circumference. This temperature configuration simulates a corium spread as shown in Figure 5.6 with an angle of repose of 7.9°. The corium-shell contact zone extends 20 cm (8 in.) vertically in the meridional direction; at 20 cm (8 in.) above the center of contact, the temperature reduces to 100°C (212°F). The contact extends 11.2°, 148 cm (58 in.), horizontally in the circumferential direction; at 11.8°, 156 cm (61 in.), from the center of contact, the temperature reduces to 100°C (212°F). At the level of the maximum temperature (center of corium contact), the radius of the liner is 760 cm (299 in.), and hence, the corium spread spans 22.4°, 290 cm (116 in.). The contact is somewhat oval in shape. Unlike the axisymmetric analysis reported previously [8], the θ -distribution of temperature is significant. In the two-dimensional analysis, the temperature was assumed to be constant around the circumference of the containment [8]. In the three-dimensional analysis, the temperature varies around the circumference of the containment.

Note that it was assumed that any temperature gradient through the shell wall was negligible.

In the present three-dimensional analyses, the heating scenario chosen corresponds to the NUREG/CR-5423 Scenario I with a 10-minute rise time. The temperature history was applied as a simple linear ramp to the temperature distributions shown in Figures 5.3 and 5.4. First, the pressure, 0.2 MPa (29.4 psi), was applied over five load increments, then the temperature was applied in 120 increments with up to ten equilibrium iterations in each increment. The analysis converged in all the increments through the temperature ramp.

As discussed below, the analysis indicates a deformation away from the presumably solidified core debris. No attempt was made to ascertain if the deformation would significantly affect the temperature distribution in the liner. The absolute magnitude of the deformation is not great, but it is enough to, perhaps, introduce additional resistance to heat transfer from the core debris to the liner.

2.3 Analysis Results

The failure results are summarized first in this section, and then other results are discussed in more detail with illustrations.

The analysis resulted in creep rupture and excessive strain failure before the final applied temperature of 1300°C (2372°F). As described above, the failure criterion developed for this work consists of a cumulative Damage Index based on the LMP. When this index exceeds unity, the time to rupture based on the temperature and the stress history during the analysis is exceeded, and thus, rupture is predicted. Due to the sensitive exponential behavior of the LMP, however, it is important to check the strains in the material to verify that they are large enough to expect rupture as the

Figure 5.3. Temperature distribution in the liner

°F	°C
378	192
544	284
710	377
876	469
1040	560
1200	649
1370	743
1540	838
1700	927
1870	1021
2030	1110
2200	1204

Figure 5.4. Temperature distribution in the region where core debris contacts the liner

Figure 5.5. The two-dimensional temperature maps of the liner (reproduced from Reference 1, Figures 6.24 and 6.25)

For the Analysis
Reported, $\alpha = 7.9^\circ$

Figure 5.6. Core debris spread during the hypothesized accident

Damage Index reaches unity. At elevated temperatures and creep conditions, a specific strain failure criterion does not exist for this material loaded in three-dimensional states of stress. However, experience suggests that strains of about 10 percent to 20 percent would be reasonable at rupture. In the present analysis, strains of this magnitude are reached at roughly the same time as the Damage Index reaches 1.0. Appendix C provides more detailed explanations of the damage model and a strain-based failure criteria.

Table 5.1 provides a summary of the effective plastic strain and Damage Index at different times in the temperature ramp and different locations in the critical zone. The critical zone is shown in Figure 5.7 where the reported locations are identified. Surface failures (possible crack initiation) are predicted around 1140°C (2090°F). Rupture is first indicated by high strains and damage at the inside liner surface below the hottest zone and at the outside of the liner surface above the hottest zone. Through-wall membrane failures are indicated by large Damage Index values at 1260°C (2300°F) in the hottest zone at the contact center. Note that by 1220°C (2230°F), effective plastic strains and Damage Index values become large at many locations in the critical zone. At the final applied temperature of 1300°C (2372°F), Damage Index values continue to grow, and at many other locations, strains become significant. Because the strains approach failure values at nearly the same time the Damage Index approaches unity, the adopted failure criteria are thought to accurately predict failure for this analysis.

More details of the response are presented in the form of deformed shapes, strain and damage index contour, profile, and history plots in Figures 5.8 through 5.19.

2.3.1 Deformations

At an applied temperature of 1143°C (2090°F) the radial deformation, looking down at a

circumferential cross section, is shown in Figure 5.8. The maximum radial displacement occurs at the center of the contact region (at the line of symmetry), and the general shape of the deformation resembles a bubble protrusion around the center of corium contact. Note that the present analysis predicts a maximum radial displacement of 2.1 cm (0.82 in.) which is smaller than the 5 cm (2 in.) space between the liner and the shield wall. Thus, the liner failure by creep rupture is predicted before contact with the wall. This figure shows the diminishing deformation away from the contact center which suggests that the cool region of the liner away from the hot section is attracting redistributed stresses from the corium contact region. The maximum temperatures of the liner at 11.2°, 148 cm (58 in.), and 11.8°, 156 cm (61 in.), from the contact center are 1143°C (2090°F) and 221°C (430°F), respectively. A two-dimensional axisymmetric analysis can not predict these deformations. In a two-dimensional analysis, the deformed shape of the liner is assumed to look like the contact center displacement around the entire circumference of the containment. In the two-dimensional model, resistance to creep is not provided by nearby circumferential areas that are cooler and stiffer. These observations demonstrate the significant difference between two-dimensional axisymmetric and three-dimensional analyses. As expected, the failure temperature predicted by the three-dimensional analysis is higher than predicted by the two-dimensional analysis.

2.3.2 Effective Plastic Strains

Effective plastic contours containing the critical regions of the liner interior and exterior at two different temperatures, 1143°C (2090°F) and 1260°C (2300°F), are shown in Figures 5.9 and 5.10, respectively. (Unless otherwise indicated, strain and damage values reported in the text are integration point predictions; values displayed in the contours are extrapolated to the nodes. Thus, differences between the contour levels and integration point data are expected.) At 1143°C

Table 5.1. Maximum effective plastic strain and Damage Index in the critical zone

Maximum Applied Temperature:			(2090°F)	1143°C	(2160°F)	1182°C	(2230°F)	1220°C	(2300°F)	1260°C
Element *	Integration point	Liner position	Effective plastic strain (%)	Damage index	Effective plastic strain (%)	Damage index	Effective plastic strain (%)	Damage index	Effective plastic strain (%)	Damage index
5	2	Temperature	(2010°F)	1100°C	(2090°F)	1140°C	(2160°F)	1180°C	(2230°F)	1220°C
		Inside	9.1	0.42	11.3	0.98	13.4	2.86	13.9	4.60
		Outside	4.7	0.07	5.2	0.07	5.3	0.07	5.5	0.08
6	1	Temperature	(2090°F)	1140°C	(2160°F)	1180°C	(2230°F)	1220°C	(2300°F)	1260°C
		Inside	9.4	0.69	9.8	1.04	10.7	2.62	10.9	4.72
		Outside	4.8	0.08	4.9	0.08	5.0	0.09	5.0	0.12
7	1	Inside	(2090°F)	1140°C	(2160°F)	1180°C	(2230°F)	1220°C	(2300°F)	1260°C
		Outside	3.9	0.20	3.9	0.25	4.3	1.59	4.7	6.30
7	2	Inside	5.3	0.40	5.3	0.52	5.3	1.14	5.4	1.47
		Outside	3.5	0.05	3.5	0.05	3.7	0.08	4.1	1.30
8	1	Inside	9.3	0.67	9.5	0.92	9.8	1.91	9.8	2.19
		Outside	3.3	0.04	3.5	0.45	3.7	0.06	3.9	0.29
9	1	Temperature	(2010°F)	1100°C	(2090°F)	1140°C	(2160°F)	1180°C	(2230°F)	1220°C
		Inside	8.4	0.55	9.6	0.99	11.1	2.50	11.3	3.06
9	3	Inside	(1830°F)	1000°C	(1900°F)	1040°C	(1970°F)	1075°C	(2010°F)	1100°C
		Outside	1.7	0.02	8.2	0.28	6.6	0.08	6.8	0.08
23	2	Inside	2.7	0.13	2.3	0.03	17.4	0.60	18.6	0.80
		Outside	1.1	0.02	3.0	0.04	4.7	0.06	4.9	0.06
26	1	Inside	1.7	0.06	5.8	0.36	11.5	0.75	12.5	0.92
		Outside	(2010°F)	1100°C	(2090°F)	1140°C	(2160°F)	1180°C	(2230°F)	1220°C
26	1	Inside	5.1	0.38	6.6	1.45	7.2	2.78	7.3	3.49
		Outside	3.7	0.06	4.1	0.07	4.2	0.07	4.2	0.09
26	1	Temperature	(2010°F)	1100°C	(2090°F)	1140°C	(2160°F)	1180°C	(2230°F)	1220°C
		Inside	2.9	0.04	7.6	1.22	3.4	0.06	3.7	0.56
		Outside	6.6	0.77	1.4	0.02	7.9	1.74	8.0	1.80

* See Figure 5.7.

Table 5.1. Maximum effective plastic strain and Damage Index in the critical zone (continued)

Maximum Applied Temperature:			(2090°F)	1143°C	(2160°F)	1182°C	(2230°F)	1220°C	(2300°F)	1260°C
Element *	Integration point	Liner position	Effective plastic strain (%)	Damage index	Effective plastic strain (%)	Damage index	Effective plastic strain (%)	Damage index	Effective plastic strain (%)	Damage index
43	4	Temperature	(2090°F)	1140°C	(2160°F)	1180°C	(2230°F)	1220°C	(2300°F)	1260°C
		Inside	6.5	0.34	6.7	0.95	7.1	3.12	7.4	8.12
		Outside	1.6	0.23	1.6	0.18	1.6	0.19	1.6	0.19

*See Figure 5.7.

Figure 5.7. Location of critical points in the high temperature zone. Results for selected points are listed in Table 5.1.

Figure 5.8. Deformed shape of the circumferential cross section at 1260°C (2300°F)

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Figure 5.9. Effective plastic strain contours of the liner inside and outside at 1143°C (2090°F)

Figure 5.10. Effective plastic strain contours of the liner inside and outside at 1260°C (2300°F)

Figure 5.11. Profiles of the maximum effective plastic strain at 1143°C (2090°F) in meridional cross sections at the center of the region contacted by the core debris and at 11.2° and 11.8° circumferentially from the center

Figure 5.12. Profiles of the maximum effective plastic strain at 1260°C (2300°F) in meridional cross sections at the center of core debris contact with the liner and at 11.2° and 11.8° circumferentially from the center

Figure 5.13. Effective plastic strain history at a critical point during heatup of the drywell liner

Figure 5.14. Damage Index contours of the liner inside and outside at 1143°C (2090°F)

Figure 5.15. Damage Index contours of the liner inside at 1260°C (2300°F)

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Figure 5.16. Damage Index contours of the liner outside at 1260°C (2300°F)

Figure 5.17. Profiles of the Damage Index at 1143°C (2090°F) in meridional cross sections at the center of core debris contact with the liner and at 11.2° and 11.8° circumferentially from the center

Figure 5.18. Profiles of the Damage Index at 1260°C (2300°F) in meridional cross sections at the center of core debris contact with the liner and at 11.2° and 11.8° circumferentially from this center

Figure 5.19. Damage Index history at a critical point during the heatup of the liner

(2090°F) on the inside of the liner, the maximum effective plastic strain contour level is about 11 percent and occurs at the corium contact center just below the hottest temperature zone. On the outside of the liner, the maximum effective plastic strain contour level is about 10 percent and occurs at the contact center just above the hottest temperature zone. In both cases, the vertical (meridional) gradient of the strain is large, and the horizontal (circumferential) gradient is small. At 5 cm (2 in.) vertically, the strain reduces to 1 percent. Horizontally, the strain remains constant for about 37 cm (15 in.) or 2.8° from the contact center. At 1260°C (2300°F) on the inside of the liner, the maximum effective plastic strain contour level is about 15 percent and occurs at the corium contact center just below the hottest temperature zone. On the outside of the liner, the maximum effective plastic strain contour level is about 18 percent. This maximum effective plastic strain occurs at the contact center about 25 cm (10 in.) above the hottest temperature zone. Note that the strain is larger than 10 percent throughout the hottest temperature zone. On both sides of the liner, the horizontal (circumferential) gradient is much larger than at 1143°C (2090°F). Horizontally, the strain reduces by 1 percent every 3.7 cm (1.5 in.) or 0.28° from the contact center.

Effective plastic strain profiles predicted at 1143°C (2090°F) are shown in Figure 5.11. The profiles follow a vertical path (meridional) in the containment at the center of corium contact and at 11.2° and 11.8° from the center. Figure 5.12 displays the strain profiles for the same locations at 1260°C (2300°F). At 1143°C (2090°F), the maximum effective plastic strain of 9.4 percent occurs on the liner outside at the contact center and about 19 cm (7.5 in.) above the concrete base floor. At 1260°C (2300°F), the maximum effective plastic strain of 18.7 percent occurs on the liner outside at the contact center and about 38 cm (15 in.) above the concrete base floor. All the strain plots indicate the greatly elevated strain due to the severe bending that occurs just above the shell-concrete junctures. At the center of

corium contact, the maximum strain at the maximum applied temperature is 18.7 percent. At 11.2° from the contact center, the maximum strain is 8 percent, and at 11.8° from the center, the wall strains are less than 0.5 percent. From these strain results, a critical zone is defined from the center of corium contact to 37 cm (15 in.) or 2.8° and between 13 and 38 cm (5 to 15 in.) above the floor.

The effective plastic strain history at a point in this critical zone is shown in Figure 5.13. As can be seen from this figure, the strain is well below 1 percent throughout most of the temperature ramp until the failure temperature is approached. At this time, the strain begins to grow to above 18 percent. In this three-dimensional analysis with a 10-minute temperature rise, the critical zone's strains begin to grow from 0.3 percent at 770°C (1420°F) to 4 percent at 904°C (1660°F) and peak at about 18 percent by 1221°C (2230°F).

2.3.3 Damage Index

Damage Index contours containing the critical regions of the liner interior and exterior at two different temperatures, 1143°C (2090°F) and 1260°C (2300°F), are shown in Figures 5.14, 5.15 and 5.16. At 1143°C (2090°F) on the inside of the liner, the maximum Damage Index contour level is 0.6 to 0.7 and occurs at the corium contact center in the hottest temperature zone. On the outside of the liner, the maximum Damage Index contour level is about 0.9 and occurs 37 cm (15 in.) or 2.8° from the contact center in the hottest temperature zone. In both cases, the vertical (meridional) gradient of the damage is large, and the horizontal (circumferential) gradient is small. At 5 cm (2 in.) vertically, the damage reduces to 0.1. Horizontally, the damage remains constant for about 37 cm (15 in.) or 2.8° from the maximum contour center. At 1260°C (2300°F) on the inside of the liner, the maximum Damage Index contour level is about 8 and occurs 111 to 148 cm (44 to 58 in.) or 8.4° to 11.2° from the

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corium contact center and just above the hottest temperature zone. On the outside of the liner, the maximum Damage Index contour level is about 6 and occurs 93 to 148 cm (36 to 58 in.) or 7° to 11.2° from contact center and just below the hottest temperature zone. Note that the Damage Index is larger than 1 throughout the hottest temperature zone. In both cases, the vertical (meridional) gradient of the damage is large, and the horizontal (circumferential) gradient is small.

Damage Index profiles predicted at 1143°C (2090°F) are shown in Figure 5.17. The profiles follow a vertical path (meridional) in the containment at the center of corium contact and at 11.2° and 11.8° from the center. Figure 5.18 displays the Damage Index profiles for the same locations at 1260°C (2300°F). At 1143°C (2090°F), the maximum Damage Index of 0.77 occurs on the liner outside at 2.8° from the contact center and about 32 cm (12.5 in.) above the concrete base floor. At 1260°C (2300°F), the maximum Damage Index of 8.1 occurs on the liner outside at 5.6° from the contact center and about 32 cm (12.5 in.) above the concrete base floor. At the center of corium contact, the maximum Damage Index during the maximum applied temperature of 1300°C (2372°F) is 19.4. At 11.2° from the contact center, the maximum Damage Index is 16, and at 11.8° from the center, the Damage Index at all locations is less than 0.15. From these damage results, a critical zone is defined between 0 to 148 cm (0 to 58 in.) or 0° to 11.2° from the center of corium contact and between 13 and 38 cm (5 to 15 in.) above the floor. This section encompasses the hottest zone of the temperature distribution.

The Damage Index history at a point in this critical zone is shown in Figure 5.19. As can be seen from this figure, the damage is nearly zero

throughout most of the temperature ramp until the failure temperature is approached. At this time, the Damage Index begins to grow to above 19. In this three-dimensional analysis with a 10-minute temperature rise, the critical zone's damages begin to grow at about 1143°C (2090°F) and continue to grow up to the final applied temperature of 1300°C (2372°F).

2.4 Comparison to Results of the Two-Dimensional Analysis

The results obtained in the three-dimensional analysis indicate that Damage Index values indicative of rupture develop at various points in the liner at temperatures as low as 1143°C (2090°F). Plastic strains at these points are also in the vicinity of expected rupture strains at 1260°C. These results can be compared to rupture predictions obtained in a previous two-dimensional, axisymmetric analysis [8]. In the case of a heating scenario similar to that used for the three-dimensional analysis in which heatup of the liner occurred over a 10-minute period, the two-dimensional axisymmetric analysis indicated liner rupture would occur at temperatures of 982 to 1100°C. Slowing the heating rate of the liner so that the temperature rise to 1300°C occurred over a two hour period yielded predicted liner rupture temperatures of 910 to 1040°C.

It is apparent from the comparisons of predicted liner rupture temperatures that limiting the region over which core debris contacts the liner reduces the vulnerability of the liner to creep rupture. The results of the two-dimensional analysis also suggest that there may be some sensitivity of the creep rupture results to details of the accident scenario. This sensitivity was not explored in the three-dimensional analysis.

3.0 Failure Temperature Distributions

The three-dimensional structural analysis reported here is an attempt to obtain a point estimate of structural failure to replace the melting failure criterion that has been used in past analyses of the Mark I liner failure issue [1-7]. There are, however, many sources of uncertainty associated with structural analyses of steel behavior at elevated temperatures, especially at temperatures above the ferrite-to-austenite phase transition at 727°C. Structural analyses of carbon steel at these elevated temperatures are by no means routine. The analysis described in the previous section was an attempt to define a "best-estimate" criterion. That is, some uncertain aspects of the problem might bias analyses to yield a higher failure temperature criterion and other uncertain aspects of the problem might lead to a lower failure temperature criterion.

Because of the uncertainties, it might be more rational to consider a distribution in the failure temperature criterion. That is, the single melting failure criterion used in the past might usefully be replaced by a creep rupture failure criterion characterized by an uncertainty distribution. This uncertainty distribution in the failure criterion could be combined with other uncertainty distributions used in the analysis of the Mark I liner failure issue.

Therefore, after the structural analysis was completed, an effort was undertaken to estimate the shape of an uncertainty distribution that could be associated with the best estimate failure temperature. A completely rigorous approach to the development of an uncertainty distribution that took into account all potentially significant contributions to the uncertainty in the creep rupture failure analysis would, of course, exceed by a wide margin the time and resources available to the project. A meaningful estimate of the distribution was sought.

The essential indicator of the rupture used in the analysis described above is based on the experimental database for the LMP. Data for

SA533 steel were used to fit the regression equation:

$$\ln(\text{LMP}) = \ln(b) + \ln[\ln \sigma_{oe}/a]$$

where

LMP = Larson-Miller Parameter,

σ_{oe} = initial stress, and

a, b = parameters adjusted to fit the data.

The data set used to determine the parametric values by linear least-squares methods consists of 13 data points. It would be expected then that predictions derived from the regression equation would have uncertainty distributions that are characterized by a Student's t distribution with 11 degrees of freedom. Then, the 100(1 - α) percent confidence interval for a predicted value derived from the regression equation is given by [15]:

$$\ln(\text{LMP}) = \langle \ln(\text{LMP}) \rangle \pm t_{(1-\alpha/2)}^{(11)} \sigma(\text{LMP})$$

where

$\ln(\text{LMP})$ = bounds of the 100 (1 - α) percent confidence interval

$\langle \ln(\text{LMP}) \rangle$ = value of the LMP derived from the regression equation

$t_{(1-\alpha/2)}^{(11)}$ = critical value of the Student's t distribution for 11 degrees of freedom at a 100 (1 - $\alpha/2$) percent confidence level.

Failure Temperature

$$\sigma(\text{LMP}) = s \left\{ \frac{1}{n} + \left[\frac{(\bar{x} - x)^2}{\sum_{i=1}^n (x(i) - \bar{x})^2} \right] \right\}^{1/2}$$

where

s = standard error in the determinations of $\ln(\text{LMP})$ in the data set
= 0.09

n = number of data points = 13

x = $\ln(\sigma_{oe})$ for prediction of $\langle \ln(\text{LMP}) \rangle$

$x(i)$ = $\ln(\sigma_{oe}(i))$ values in the data set

$$\bar{x} = (1/n) \sum_{i=1}^n x(i)$$

Note that to obtain a $100(1 - \alpha)$ percent confidence level for the predicted value of $\ln(\text{LMP})$, the critical value of the Student's t distribution is evaluated at the $100(1 - \alpha/2)$ percent confidence level. This is, of course, because uncertainties leading to values both above and below the prediction are of interest. Critical values of the Student's t distribution at various confidence levels are:

$100(1 - \alpha)\%$	$\alpha/2$	$t_{(1 - \alpha/2)}^{(11)}$
50	0.25	0.6974
80	0.10	1.363
90	0.05	1.796
95	0.025	2.201
98	0.010	2.718
99	0.005	3.106

From these critical values and the expressions above, it is evident that the confidence bounds on the values of $\ln(\text{LMP})$ derived from the regression equation increase as:

- conditions deviate from the mean of the database, and
- the level of confidence being demanded increases.

The distribution of values of the LMP that are derived from the limited, available, database can be used to derive a distribution in the temperature needed to produce a value of the Damage Index, D , equal to 1. The Damage Index is given by:

$$D = \sum_{j=1}^N \frac{\Delta t(j)}{t_r(\sigma(j), T(j))}$$

where

$\Delta t(j)$ = time increment in hours associated with the j^{th} step in the temperature ramp i ,

$$t_r(\sigma(j), T(j)) = \exp \left\{ 2.303 \left[\frac{\ln(\text{LMP}(j))}{0.001T(j)} - 20 \right] \right\}, \text{ and}$$

$T(j)$ = temperature in Rankine during the j^{th} step in the temperature ramp.

For the scenario hypothesized in the structural analysis, temperature increases at the rate of about 7800°C/hr . For typical elements in the structure, most of the damage accumulates in the final time step. For instance, at the inner surface of element 7:

				Failure Temperature	
T(°C)	T(°R)	D	LMP	Probability (%)	Temperature (°C)
1140	2540	0.2		0.5	1180.7
1180	2615	0.25	49.0	1.0	1181.0
1220	2687	1.59	46.6	2.5	1182.0
1260	2759	6.20	46.4	5.0	1183.2
				10.0	1185.4

The Damage Index reaches $D = 1$ during the temperature interval from 1180 to 1220°C. The LMP used in the analysis for this interval is 46.6. The confidence intervals for the LMP derived from its Student's t distribution are:

Confidence Level 100(1 - α)%	Range for LMP
---	---------------

80	48.8 to 44.5
90	49.5 to 43.9
95	50.2 to 43.3
98	51.1 to 42.5
99	51.8 to 42.0

The cumulative distribution for values of the LMP parameter is then characterized by:

Probability (%) that LMP is less	LMP
0.5	42.0
1.0	42.5
2.5	43.3
5.0	43.9
10.0	44.5
50.0	46.6
90.0	48.8
95.0	49.5
97.5	50.2
99.0	51.1
99.5	51.8

The distributed values of the LMP can then be used to determine the distribution of the values of temperature at which $D = 1$:

50.0	1201.4
90.0	1235.4
95.0	1249.3
97.5	1263.4
99.0	1282.6
99.5	1297.9

The cumulative probability distribution for the failure temperature in element 7 is shown in Figure 5.20, and the associated probability density function is shown in Figure 5.21. As expected the distribution sharply rises from 1180°C and has a long tail extending to temperatures above 1220°C.

Similar analyses have been done for the outside element 7, outside of element 9, the inside of element 23, and the inside of element 43. Results are shown in Table 5.2.

These various distributions for the rupture temperature exhibit a similarity of shape around a median. The distributions have, however, been computed considering only the Damage Index as an indicator of rupture. The rupture strain criterion has been neglected.

Rupture strains are also estimated by a correlation of data. A distribution for predicted rupture strains in terms of a Student's t distribution can also be defined. If it is assumed that strain and the Damage Index are independent (which they are not), then a joint distribution of temperatures at which the Damage Index is one and the effective plastic strain exceeds the rupture strain could, in principle, be formulated. Unfortunately,

Figure 5.20. Cumulative probability distribution for the temperature of rupture at the inside of element 7. Temperature of rupture has been taken to be the temperature at which the Damage Index equals 1. The strain criterion is neglected in formulating this distribution.

Figure 5.21. Probability density function for the temperature of rupture at the inside of element 7. Temperature of rupture has been taken to be the temperature at which the Damage Index equals 1.

Failure Temperature

Table 5.2. Cumulative probability distributions for the rupture temperature at various locations on the liner

Cumulative probability (%) of a lower failure temperature	Failure temperature (°C) at**				
	Element 7 Inside Int. pt. 1	Element 7 Outside Int. pt. 1	Element 9 Outside Int. pt. 1	Element 23 Inside Int. pt. 2	Element 43 Inside Int. pt. 4
0.5	1180.7	1220.3	1101.5	1104.2	1142.1
1.0	1181.0	1220.5	1102.4	1106.5	1143.3
2.5	1182.0	1220.9	1104.5	1111.4	1146.0
5.0	1183.2	1221.5	1107.2	1116.7	1149.3
10.0	1185.4	1222.5	1111.5	1124.4	1154.4
50.0	1201.4	1231.7	1136.7	1158.9	1182.1
90.0	1235.4	1257.4	1177.4	1205.5	1224.6
95.0	1249.3	1269.4	1192.6	1222.2	1240.4
97.5	1263.4	1282.0	1207.6	1238.3	1255.9
99.0	1282.6	1299.7	1227.6	1259.6	1276.5
99.5	1297.9	(1314.0)*	1243.3	1276.1	1292.6

*Temperature ramp hypothesized for the analysis went to only 1300°C.

**See Table 1 and Figure 7 for further information on the locations of the elements of the computational grid.

there is not sufficient information available from the structural analyses to compute the entire distribution. Partial distributions computed in this way suggest that the shapes of the distributions are rather similar to those shown in Table 5.2, but the medians are shifted to slightly higher temperatures.

The distributions in rupture temperature deduced by considering only the uncertainty in the LMP have an intuitive appeal with respect to their shapes. The absolute value of the rupture temperature (say the median temperature)

predicted from the Damage Index is not thought reliable because of the limitations in the available data for the LMPs. A plausible, though obviously very approximate uncertainty distribution can then be formulated by considering temperature deviations from a median value. The absolute location of the median (or any other selected quantile of the distribution) can then be deduced by considering both the Damage Index and the strain criterion for rupture. Such a distribution of deviations from an unspecified median is shown in Figure 5.22 and is listed in Table 5.3.

Figure 5.22. Cumulative probability distribution for deviations from a selected median temperature of liner rupture

**Table 5.3. Uncertainty distribution for temperature deviations
from a selected temperature of liner rupture**

Probability (%) of a Lower ΔT	ΔT (°C)	Probability Density (°C ⁻¹)
0.5	-24.5	9.8×10^{-3}
1.0	-24.0	0.012
2.5	-22.8	0.016
5.0	-21.2	0.019
10	-18.5	0.021
20	-13.9	0.0225
30	-9.5	0.0226
40	-4.9	0.0216
50	0	0.019
60	5.6	0.018
70	12.4	0.014
80	21.4	0.010
90	36.0	5.9×10^{-3}
95	50.4	3.5×10^{-3}
97.5	64.9	1.7×10^{-3}
99	84.4	7.7×10^{-4}
99.5	99.9	3.2×10^{-4}

4.0 Conclusions

Though the structural analysis reported here is limited in scope, it does lead to some conclusions pertinent to the issue of the vulnerability of the Mark I drywell liner to direct attack by core debris:

1. Heating of the liner by core debris produces substantial deformations of the liner in the local regions of contact with core debris. Deformations are, however, not so large that the liner can derive support from the concrete shield wall of the reactor.
2. The Damage Index failure criterion based on the LMP has to be coupled with at least a qualitative examination of rupture strains to obtain appealing predictions of liner failure. Damage Index values indicative of rupture on both the inner and outer surface of the liner and large plastic strains were calculated to develop in the liner at 1260°C in the vicinity of the center of core debris contact with the liner. This temperature is about 240°C less than the melting temperature which has been used as the failure criterion in previous analyses of the Mark I liner. An approximate uncertainty distribution for this value of the rupture temperature was derived by analysis of the uncertainty in predictions of the LMP from the available database. This uncertainty analysis indicates there to be a 95 percent confidence that for the scenario being analyzed the creep rupture failure temperature will not be less than 1238°C. The uncertainty analysis also indicates there to be a 95 percent confidence that the creep rupture failure temperature will be less than 1310°C.
3. The inability to use the Damage Index method alone to obtain satisfactory predictions of the creep rupture of the Mark I liner when heated by core debris probably stems from the limitations in the available database for the LMP. Experimental determinations of this database have come from constant load and constant creep rate tests. Such data cannot be directly applied to situations involving varying temperatures and varying stresses.
4. In addition to the phenomenological uncertainties cited above, there are uncertainties in the boundary conditions for structural analyses. Variations in the heating rate of the liner can reasonably be expected to affect the creep history of the liner. Certainly in past two-dimensional analyses, it has been found that slowing the rate at which the liner heated reduced the temperature at which rupture was predicted to occur. The amount of the liner that is in contact with the debris can also affect the predicted temperature of rupture.

5.0 References

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Appendix A

Material Properties Data

For the creep-rupture analyses performed in this study, a high temperature constitutive characterization of the Mark I containment liner is required. However, until now, interests in high temperature creep rupture have focused on the reactor vessel material instead of liner material. The data available for the present study are derived from literature characterizing reactor vessel steel, namely, SA533-B1. Although the liner is made of SA516-70 steel, the use of the SA533-B1 creep data can be justified on the basis that fine-grained steels behave similarly at high temperatures. Two sources provided creep data: a Combustion Engineering Inc. (CEI) report prepared for the Electric Power Research Institute (EPRI) [A-1], and an Idaho National Engineering Laboratory (INEL) report prepared for the U.S. Nuclear Regulatory Commission (NRC) [A-2]. Both reports describe high temperature creep experiments for SA533-B1 steel.

A.1 Creep Law

Combustion Engineering performed creep experiments at moderately high temperatures ranging from 399 to 649°C (750° to 1200°F). INEL performed experiments at high temperatures ranging from 627°C to 1100°C (1160° to 2012° F). At each temperature, several constant load creep tests were reported. A creep power law, shown in Equation (A-1), is used to fit the data where, ϵ_c is the creep strain, σ is the applied initial stress in ksi, and t is the time in hours.

$$\epsilon_c = A \sigma^r t^s \quad (A-1)$$

The creep strain rate is:

$$\dot{\epsilon}_c = A s \sigma^r t^{s-1} \quad (A-2)$$

The parameters A , r , and s in Equation (A-1) are dependent on temperature but independent of stress. The form of this equation was suggested in Reference [A-2], but the parameters A , r , and s were not quantified and had to be determined as part of the present study. This was done by digitizing the creep curves in Reference [A-2] and applying a least squares fit to the data. The resulting expressions for A , r , and s were incorporated into the elastic-viscoplastic constitutive model described in Appendix B.

The differences between the response of the liner and pressure vessel steels are assumed to be negligible at high temperatures due to their similar fine-grain structure. At high temperatures, the material undergoes a heat treatment that negates the differences between the two materials. B. C. Gowda, [A-3] examined the temperature dependence of the yield stress for SA516-55 steel, and this data, compared with the CEI, INEL, and ASME Boiler and Pressure Vessel Code [A-4] data, suggest that SA516 and SA533 steels behave similarly after the phase change at 727°C (1340°F). Figure A-1 shows the yield stress temperature dependence for the SA516 and SA533 steels at various strain rates. Before the phase change, the trends in the temperature dependencies of yield stresses for both steels are similar even though the yield magnitudes are different. After the phase change, the yield stress temperature trends and magnitudes for all strain rates converge to a single value (within normal scatter) for both materials. Since the focus of this study is on the high temperature creep rupture modes, the SA533 data are deemed adequate for the formulation of an approximate creep law.

A.2 EPRI Creep Experiments

CEI, in a topical report to EPRI [A-1], reported constant load creep data for SA533 steel from

Figure A-1. Yield stress and modulus dependence on temperature

23 experiments; Table A-1 summarizes the experiments reported. Construction of the creep power law used data from 15 experiments. Table A-2 summarizes the experiments used. The experiments in Table A-2 were chosen because creep histories for these experiments were provided; the other experiments were reported, but creep histories were not available in the report. Creep data from the experiments are shown in Figure A-2. Note that all tests are completed over the same time history.

A.3 INEL Creep Experiments

INEL [A-2] detailed constant load creep data for SA533 steel from 13 experiments. Table A-3 summarizes the experiments reported. Construction of the creep power law used data from all the experiments. Creep data from the experiments are shown in Figures A-3 through A-6. Note that all tests are completed over a wide range of time histories. Thus, the strain rates for these experiments vary substantially.

A.4 Creep Data Fit

To provide a fit to the creep data discussed above, the parameters A , r , and s of the power law in Equation (A-1) are determined using the least squares method. (The constants are valid for stresses in ksi and time in hours.) The parameters were held constant with stress and strain rate and allowed to vary only with temperature. Separate fits were performed for the primary/secondary creep portion and the tertiary creep portion.

The structure of the power law does not lend itself to existing automated least squares fit methodologies but requires optimization techniques. However, optimization techniques [A-5] require a level of effort beyond the scope of this study, and thus, a brute force automated least squares fit methodology [A-6] was devised to determine the constants. This methodology minimizes the least squares error shown in

Equation (A-3) of all the data points for the constants A , r , and s .

$$\text{error} = \left(\sum (\epsilon_c - A \sigma^r t^s)^2 \right)^{1/2} \quad (\text{A-3})$$

A large number of computations is required to provide a reasonable fit; for example, 100 trials each for 3 constants leads to 300 calculations per data point. Table A-4 contains the constants at each temperature as determined by this approach, and Figures A-2 through A-6 show the predicted curves compared to the experimental creep data. Again, note that these constants are valid for stresses in ksi and time in hours. The values of A , r , and s are linearly interpolated between 399° and 1100°C (750° and 2012°F). Outside this range, A , r , and s are held constant. This restriction implies that for temperatures below 399°C (750°F), the creep strain may be overestimated, and for temperatures above 1100°C (2012°F), the creep strain may be underestimated. In Figures A-3 through A-6, notice that the valid portions of the curves are the primary/secondary curve up to the intersection point with the tertiary part and then the tertiary curve. In implementing the creep equation in the constitutive model (see Appendix B), the model automatically switches to the tertiary curve after the point of intersection of the primary/secondary curve with the tertiary curve. This process required a separate state variable that keeps track of the intersection point.

A.5 Yield Strength and Modulus Data

The dependence of the yield strength and elastic modulus on temperature are shown in Figure A-1. The yield strength fit used in the analysis is shown with a solid line in the figure. The low-temperature (CE) data and the high-temperature (INEL) data were fit separately, and the two curves were joined by interpolation. Similarly, the elastic modulus data were fit separately for the low-temperature and the high-temperature regimes and then joined by interpolation.

Table A-1. Constant load creep experiment for SA533 steel reported by Combustion Engineering

Temperature °F (°C)	750 (399)	900 (482)	1050 (564)	1200 (649)
Applied Stress	30.0 (207)	14.0 (97)	10.0 (69)	2.0 (14)
KSI (MPa)	45.0 (310)	18.0 (124)	14.0 (97)	3.0 (21)
	50.0 (345)	22.0 (152)	18.0 (124)	4.0 (28)
	55.0 (379)	26.0 (179)	20.0 (138)	5.0 (34)
	60.0 (414)	30.0 (207)	22.0 (152)	6.0 (41)
		35.0 (241)		
		40.0 (276)		
		44.0 (303)		

Table A-2. Constant load creep experiment used in the creep power law construction

Temperature °F (°C)	750 (399)	900 (482)	1050 (564)	1200 (649)
Applied Stress	45.0 (310)	26.0 (179)	10.0 (69)	3.0 (21)
KSI (MPa)	50.0 (345)	30.0 (207)	14.0 (97)	4.0 (28)
	55.0 (379)	35.0 (241)	18.0 (124)	5.0 (34)
		44.0 (303)	22.0 (152)	6.0 (41)

Table A-3. Constant load creep experiment configurations for SA533 steel reported by INEL

Temperature °F (°C)	1160 (627)	1340 (727)	1430 (777)	1610 (877)	1790 (977)	2012 (1100)
Applied Stress	10.1 (70)	5.7 (39)	2.0 (14)	1.8 (12.5)	1.2 (8.0)	0.5 (3.5)
KSI (MPa)	20.0 (140)	8.1 (56)	3.8 (26)	3.8 (26.5)	1.8 (12.5)	1.0 (7.0)
					3.8 (26.5)

Figure A-2. Creep data for 399°C (750°F), 482°C (900°F), 566°C (1050°F), and 649°C (1200°F)

Figure A-3. Creep data for 627°C (1160°F) and 727°C (1340°F)

Figure A-4. Creep data for 777°C (1430°F) and 877°C (1610°F)

Figure A-5. Creep data for 977°C (1790°F)

STRESS = .5 ksi = 3.4 MPa **STRESS = 1.0 ksi = 6.9 MPa**

Figure A-6. Creep data for 1100°C (2012°F)

Table A-4. Creep power law constants determined by least squares approximation

$\epsilon_c = A \sigma^r t^s$	Primary/secondary creep power law constants			Tertiary creep power law constants		
Temperature °F (°C)	A	r	s	A	r	s
750 (399)	3.17E-16	7.01	0.243	-	-	-
900 (482)	3.76E-10	3.83	0.405	-	-	-
1050 (564)	4.90E-9	3.96	0.624	-	-	-
1160 (627)	1.06E-6	2.83	0.836	2.18E-11	5.93	1.69
1200 (649)	2.64E-5	1.76	0.564	-	-	-
1340 (627)	1.25E-3	1.77	0.678	1.05E-4	2.55	1.75
1430 (777)	6.62E-4	2.66	0.704	6.52E-7	6.75	1.53
1610 (877)	1.08E-3	3.00	0.788	5.82E-5	4.73	1.38
1790 (977)	8.56E-3	3.67	0.536	1.20E-4	11.1	1.57
2012 (1100)	0.236	4.74	0.714	0.748	11.6	1.82

A.6 References

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Appendix B

Viscoplastic Constitutive Model

The constitutive formulation presented here is based on a rate-dependent plasticity formulation [Y. R. Rashid et al., "A Rate-Dependent Plasticity Model for High Temperature Metals," to be published] in which the material's deviatoric response is viscoplastic and the dilatational (volumetric) response is elastic. Thus, the plastic strains are time dependent and are determined from a flow potential that is a continuous function of stress, temperature, and time. The material data utilized to quantify the parameters of the constitutive model were presented in Appendix A. Before presenting the three-dimensional constitutive equations of the material model, it is necessary to discuss the use of the experimental data in finite strain analysis.

B.1 Implementation of Material Data

The creep data discussed in Appendix A were obtained from constant load tests, and therefore, the stress variable in the creep equation is the initial stress rather than the current stress. Also, the creep strain is the nominal strain rather than the logarithmic strain. Therefore, it is necessary that the stress and strain variables be redefined before they are used in the constitutive model. This is discussed below.

It is of interest to note that the tertiary creep observed in a constant load creep test may be thought of as a geometric effect due to the reduction in area rather than a material effect. Consequently, the analysis may utilize the minimum creep rate (ignoring the small primary creep part) thereby greatly simplifying the curve fitting process. However, the limited available data for constant stress tests also show tertiary creep, but its time range is significantly shorter than its counterpart in the constant load tests. Closer examination of the data would show that the tertiary creep in a constant stress test may still be a geometric effective, perhaps because of the inability of the testing machine to shed the load

when the test specimen gets closer to failure by localized necking. The onset of the tertiary creep in a constant stress test marks the beginning of localized necking and can therefore be called the true instability strain. This instability strain and the corresponding true stress may be regarded as the best failure measures since the measurements beyond that point become highly unreliable. This is due to the fact that the reduction-in-area data are affected by local imperfections, causing localized necking that differs from specimen to specimen, thus producing large scatter in both the reduction-in-area and the elongation data. If we were to regard the instability strain from a constant stress test to be the failure strain, then the failure stress would be the true stress at that instability strain. This will be discussed further under failure criteria. Since constant-stress creep data are not available, it is necessary to develop a procedure for using the constant-load data. To this end, let the principal stretches be λ_1 , λ_2 , and λ_3 . Then

$$\lambda_1 \lambda_2 \lambda_3 = 1 \quad (\text{B-1})$$

In a tension test, λ_1 is in the direction of the load and is a measure of the strain that is recorded in the tests. The product $\lambda_2 \lambda_3$ is a measure of the area of the specimen. The logarithmic strain, ϵ_L , which is the strain that is conjugate to the true stress, is the strain measure normally used in finite strain analysis. Inelastic constitutive laws are best expressed in terms of the true stress and logarithmic strain, because when plotted against each other they match the experimental data best. The logarithmic strain is given by

$$\epsilon_L = \ln(\lambda_1) \quad (\text{B-2})$$

The nominal strain ϵ_N , which is the strain that is usually recorded in an experiment, is

Appendix B

$$\epsilon_N = \frac{\Delta L}{L_0} = \lambda_1 - 1 \quad (B-3)$$

and

$$\lambda_1 = e^{\epsilon} L = \epsilon_N + 1. \quad (B-4)$$

Since $\lambda_2 \lambda_3 = \text{Area} = 1/\lambda_1$, then

$$\text{Area} = e^{-\epsilon} L = 1/(1 + \epsilon_N) \quad (B-5)$$

and the reduction in area is

$$\Delta A = 1 - e^{-\epsilon} L = \epsilon_N/(1 + \epsilon_N) \quad (B-6)$$

Therefore, using either strain measures ϵ_L or ϵ_N , the calculated reduction in area is always smaller than the elongation. However, the data would show "measured" area reductions that are much higher than the elongation, indicating localized necking that is non-uniform over the gauge length L_0 .

The nominal creep rate, from Equation (A-2), can be written as

$$\dot{\epsilon}_N^c = A s \sigma_0^r t^{s-1} \quad (B-7)$$

where σ_0 is the initial stress in the specimen and A , r , and s are determined from data. The above equation is an initial condition to a constant-load, variable-stress creep test. In such a test the time-dependent true stress σ evolves as follows:

$$\sigma = \sigma_0(1 + \epsilon_N) \quad (B-8)$$

where ϵ_N is the total time-dependent nominal strain. In terms of the logarithmic strain, the true stress becomes

$$\sigma = \sigma_0 e^{\epsilon} L \quad (B-9)$$

In finite strain analyses, the true stresses and the logarithmic strains are the calculated variables. Therefore, it is necessary to convert the calculated true stresses to the nominal stresses before entering the material data routines. Furthermore, since the material data are one-dimensional, the one-dimensional equivalence, namely the effective stress, of the calculated three-dimensional stress state must first be calculated. This is done as follows:

$$\sigma_{oi} = \sigma_i/\lambda_i = \sigma_i e^{-\epsilon} L_i$$

where

ϵ_{Li} = the i th principal logarithmic strain,

σ_i = the i th stress component (assumed here to be principal value) in the direction of ϵ_{Li} , and

σ_{oi} = the i th principal stress referenced to the undeformed geometry.

The effective stress referenced to the undeformed (initial) geometry becomes

$$\sigma_{oe} = \left[\frac{1}{2} \left((\sigma_{01} - \sigma_{02})^2 + (\sigma_{02} - \sigma_{03})^2 + (\sigma_{03} - \sigma_{01})^2 \right) \right]^{1/2} \quad (B-11)$$

This "initial" effective stress would be the uniaxial equivalent stress to be used in the material data expressions Equations (A-1) and (A-2) in Appendix A.

B.2 Uniaxial Constitutive Model

A rate-dependent uniaxial material response can be expressed as follows:

$$\sigma = K \epsilon^n \left(\frac{\dot{\epsilon}}{\dot{\epsilon}_r} \right)^m \quad (\text{B-12})$$

where

σ = true or Cauchy stress,

ϵ = logarithmic strain,

$\dot{\epsilon}$ = logarithmic strain rate,

$\dot{\epsilon}_r$ = reference strain rate,

K = strength coefficient, function of temperature and has units of stress,

n = hardness coefficient, function of temperature, and

m = strain-rate sensitivity coefficient, function of temperature

Applying Equation (B-12) to a constant-load creep test, we substitute Equation (B-9) for σ and solve for $\dot{\epsilon}_c$ to obtain

$$\dot{\epsilon}_c = \dot{\epsilon}_r \left(\frac{\sigma_0}{K} \right)^{\frac{1}{3}} \left(e^{-\epsilon_c} \epsilon_c^{-n} \right)^{\frac{1}{m}} \quad (\text{B-13})$$

where

$\dot{\epsilon}_c$ = logarithmic creep strain rate at time t ,

ϵ_c = integrated logarithmic creep strain, and

σ_0 = initial applied stress.

Since the measured creep strain is the nominal ϵ_{NC} , it is necessary to convert the measured ϵ_{NC}

to the logarithmic strain ϵ_c using Equation (B-4) before Equation (B-13) can be used to calculate the logarithmic creep rate. In order to make use of the material data reported in Appendix A, the model parameters K , m , and n can be expressed in terms of the material parameters A , r , and s as follows:

$$\begin{aligned} K &= (s^s A)^{-\frac{1}{r}}, \\ n &= \frac{1 - S}{r}, \\ m &= \frac{s}{r} \end{aligned} \quad (\text{B-14})$$

B.3 Three-Dimensional Constitutive Formulation

It should be noted that the stress and strain variables in the following derivations are respectively true stress and logarithmic strain unless stated otherwise.

The deviatoric strain rates are decomposed into elastic and plastic components as follows:

$$\dot{\epsilon}_{ij} = \dot{\epsilon}_{ij}^e + \dot{\epsilon}_{ij}^p \quad (\text{B-15})$$

We assume that a flow potential F exists such that

$$\dot{\epsilon}_{ij}^p = \lambda \frac{\partial F}{\partial S_{ij}} \quad (\text{B-16})$$

and

$$F = \frac{1}{2} S_{ij} S_{ij} - \kappa^2 \quad (\text{B-17})$$

where κ is temperature dependent and the deviatoric stress S_{ij} are functions of the plastic strains:

$$S_{ij} = S_{ij}(\epsilon_{mn}^p) \quad (B-18)$$

The effective strain rate is:

$$\dot{\epsilon}_e = \left(\frac{2}{3} \dot{\epsilon}_{ij} \dot{\epsilon}_{ij} \right)^{1/2} \quad (B-19)$$

where

$$\dot{\epsilon}_{ij} = J S_{ij} \quad (B-20)$$

and J is the material viscoplastic compliance determined from the data given in Equation (B-13), namely,

$$J = \frac{3\dot{\epsilon}_r}{2\sigma_e} \left(\frac{\sigma_{oe}}{K} \right)^{1/m} \left(e^{-\epsilon_e} \epsilon_e^{-n} \right)^{1/m} \quad (B-21)$$

From the consistency condition, we have

$$dF = \frac{\partial F}{\partial S_{ij}} dS_{ij} + \frac{\partial F}{\partial \epsilon_{ij}^p} d\epsilon_{ij}^p + \frac{\partial F}{\partial \kappa} d\kappa = 0 \quad (B-22)$$

Making use of the following:

$$\begin{aligned} \frac{\partial F}{\partial S_{ij}} &= S_{ij}, \quad \frac{\partial F}{\partial \epsilon_{ij}^p} = -\frac{2}{3} E' S_{ij}, \\ d\epsilon_{ij}^p &= \lambda dt S_{ij} = d\lambda S_{ij} \end{aligned}$$

we have,

$$dF = S_{ij} dS_{ij} - \frac{2}{3} E' S_{ij} d\lambda S_{ij} - 2\kappa d\kappa = 0 \quad (B-23)$$

from the elastic relations

$$\begin{aligned} dS_{ij} &= 2\mu (d\epsilon_{ij}^e) = 2\mu (d\epsilon_{ij} - d\epsilon_{ij}^p) \\ &= 2\mu (d\epsilon_{ij} - d\lambda S_{ij}) \end{aligned} \quad (B-24)$$

In the above, μ is the shear modulus and E' is the plastic modulus which is the slope of the stress-plastic strain curve.

Substituting Equation (B-24) into Equation (B-23) yields:

$$\begin{aligned} dF &= 2\mu S_{ij} d\epsilon_{ij} - 2\mu d\lambda S_{ij} S_{ij} \\ &\quad - \frac{2}{3} E' d\lambda S_{ij} S_{ij} - 2\kappa d\kappa = 0 \end{aligned} \quad (B-25)$$

Solving for $d\lambda$ from Equation (B-25), we have:

$$d\lambda = \frac{S_{ij} d\epsilon_{ij}}{R} - \frac{\kappa d\kappa}{\mu R} \quad (B-26)$$

where

$$R = 2\kappa^2 (1 + E'/3\mu) \quad (B-27)$$

Substituting Equation (B-20) into Equation (B-26) gives:

$$d\lambda = \frac{J}{R} S_{ij} S_{ij} - \frac{\kappa d\kappa}{\mu R} \quad (B-28)$$

Now, for convenience, we will continue the derivation using matrix notation:

$$d\lambda = \frac{J}{R} \underline{S}^T \underline{S} - \frac{\kappa d\kappa}{\mu R} \quad (B-29)$$

From elastic relations we have for the deviatoric stress vector:

$$d\tilde{S} = 2\mu d\tilde{\epsilon}^e = 2\mu\tilde{H}_2 d\tilde{\epsilon}^e \quad (B-30)$$

and for the pressure

$$dP = \frac{d\sigma_{kk}}{3} = \frac{\beta}{3}\tilde{H}_1 d\tilde{\epsilon} \quad (B-31)$$

where β is the bulk modulus and $\tilde{H}_1$ and $\tilde{H}_2$ are numerical matrices to be defined below. Combining Equations (B-30) and (B-31):

$$d\tilde{\sigma} = d\tilde{S} + dp\tilde{\delta} = 2\mu\tilde{H}_2 d\tilde{\epsilon}^2 + \tilde{H}_2 d\tilde{\epsilon}^e \quad (B-32)$$

where $d\sigma$ are the coordinate stresses and $\tilde{\delta}$ is the Kronecker delta.

$$d\tilde{\sigma} = 2\mu\tilde{H}_2 (d\tilde{\epsilon} - d\tilde{\epsilon}^P) + \frac{\beta}{3}\tilde{H}_1 d\tilde{\epsilon} \quad (B-33)$$

or,

$$d\tilde{\sigma} = \tilde{H} d\tilde{\epsilon} - 2\mu\tilde{H}_2 \tilde{S} d\lambda \quad (B-34)$$

$$\text{where } \tilde{H} = 2\mu\tilde{H}_2 + \frac{\beta}{3}\tilde{H}_1 \quad (B-35)$$

is the elasticity matrix.

Substituting Equation (B-29) into Equation (B-34),

$$d\tilde{\sigma} = \tilde{H} d\tilde{\epsilon} - 2\mu\tilde{H}_2 \left(\tilde{S} \tilde{S}^T \frac{J}{R} - \frac{\kappa d\kappa}{\mu R} \tilde{I} \right) \tilde{S} \quad (B-36)$$

where $\tilde{I}$ is the identity matrix.

Substituting $\tilde{H}_2 \tilde{\sigma}$ for $\tilde{S}$ and writing Equation (B-36) for time step n gives,

$$d\tilde{\sigma}_n = \tilde{H} d\tilde{\epsilon}_n - \tilde{M}_n \tilde{\sigma}_n \quad (B-37)$$

$$\text{where } \tilde{M} = 2\mu\tilde{H}_2 \left(\frac{J}{R} \tilde{S} \tilde{S}^T - \frac{\kappa d\kappa}{\mu R} \tilde{I} \right) \tilde{H}_2 \quad (B-38)$$

$$\text{But } \tilde{\sigma}_n = \tilde{\sigma}_{n-1} + d\tilde{\sigma}_n \quad (B-39)$$

Then:

$$\Delta\tilde{\sigma}_n = \left[(\tilde{I} + \tilde{M}_n)^{-1} \tilde{H} \right] \left\{ \Delta\tilde{\epsilon}_n - \tilde{H}^{-1} \tilde{M}_n \tilde{\sigma}_{n-1} \right\} \quad (B-40)$$

Equation (B-40) is the incremental stress-strain relations that will be used in the finite element formulation.

Referring to Equation (B-12) and taking $\dot{\epsilon}_r = 1/\text{hr}$, we can derive an expression for the yield (flow) stress by setting $\epsilon = \sigma_y/E$ and $\sigma = \sigma_y$ as follows:

$$\sigma_y = \left(\frac{K}{E^n} \right)^{\frac{1}{1-n}} (\dot{\epsilon})^{\frac{m}{1-n}} \quad (B-41)$$

and

$$\kappa = \left(\frac{K}{E^n} \right)^{\frac{1}{1-n}} \dot{\epsilon}^{\frac{m}{1-n}} / \sqrt{3} \quad (B-42)$$

As can be seen the yield stress is dependent on the strain rate, and the incremental stress-strain relations, Equation (B-40), are function of the strain rate not only through the creep compliance J but also the parameter κ . The material parameter K , m , and n are determined from data through the expressions B-14 and the creep curves presented in Appendix A. The following definitions are used in the above equations:

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$$\mu = \frac{E}{2(1 + \nu)} ,$$

E = elastic modulus,

E' = plastic modulus,

ν = Poisson's ratio,

$$\underline{S}^T = (S_{11}, S_{22}, S_{33}, S_{12}, S_{23}, S_{13}),$$

$$\underline{\underline{H}}_1 = \begin{bmatrix} 1 & 1 & 1 & 0 & 0 & 0 \\ & 1 & 1 & 0 & 0 & 0 \\ \text{Symm} & 1 & 0 & 0 & 0 & 0 \\ & & 0 & 0 & 0 & 0 \\ & & & 0 & 0 & 0 \\ & & & & 0 & 0 \end{bmatrix} ,$$

$$\underline{\underline{H}}_2 = \begin{bmatrix} \frac{2}{3} & -\frac{1}{3} & -\frac{1}{3} & 0 & 0 & 0 \\ & \frac{2}{3} & -\frac{1}{3} & 0 & 0 & 0 \\ \text{Symm} & \frac{2}{3} & 0 & 0 & 0 & 0 \\ & & 1 & 0 & 0 & 0 \\ & & & 1 & 0 & 0 \\ & & & & 1 & 0 \\ & & & & & 1 \end{bmatrix} ;$$

$$\underline{\underline{H}} = \frac{\nu E}{(1 + \nu)(1 - 2\nu)}$$

$$\begin{bmatrix} \frac{1-\nu}{\nu} & 1 & 1 & 0 & 0 & 0 \\ & \frac{1-\nu}{\nu} & 1 & 0 & 0 & 0 \\ \text{Symm} & \frac{1-\nu}{\nu} & 0 & 0 & 0 & 0 \\ & & \frac{1-2\nu}{2\nu} & 0 & 0 & 0 \\ & & & \frac{1-2\nu}{2\nu} & 0 & 0 \\ & & & & \frac{1-2\nu}{2\nu} & 0 \\ & & & & & \frac{1-2\nu}{2\nu} \end{bmatrix}$$

$$\sigma_e = (3/2 S_{ij} S_{ij})^{1/2}$$

$$\epsilon_e = (2/3 e_{ij} e_{ij})^{1/2}$$

$$S_{ij} = \sigma_{ij} - 1/3 \sigma_{kk} d_{ij}$$

$$e_{ij} = e_{ij} - 1/3 \epsilon_{kk} d_{ij}$$

Equation (B-40) is used to derive the element stiffness and internal force vectors. Stresses calculated from Equation (B-40) are adjusted, using the radial return correction, to remain consistent with the flow potential as follows:

$$\underline{g}_n^{\text{final}} = (\underline{g}_{n-1} + \Delta \underline{g}_n) \left(1 - \left(\frac{2\kappa^2}{S_{ij} S_{ij}} \right)^{1/2} \right) \quad (\text{B-43})$$

Appendix C

Creep-Rupture Failure Model for Steel Under Elevated Temperatures

The creep-rupture failure model developed in the present study is based on the cumulative damage concept. The damage increment is defined as the ratio of the time increment to the rupture time. The rupture time is calculated from the current temperature and stress. The Damage Index is obtained by summing the incremental damages. The Damage Index is unity at failure by creep rupture. The Larson-Miller Parameter (LMP) is determined from constant load, constant temperature creep tests [C-1]. Data from INEL [C-2] were used to express the LMP as a function of the applied stress. This appendix describes the use of the LMP in the cumulative damage failure model and its application to time varying stresses and temperatures.

C.1 Creep-Rupture Data Summary

INEL reported creep rupture times for the SA533 steel at temperatures ranging from 1160°F (627°C) to 2012°F (1100°C) [C-2]. LMP values were calculated using Equation (C-1), where t_r is the time to rupture in hours and T is the temperature in Rankine.

$$\text{LMP} = .001(20.0 + \log_{10} t_r)T \quad (\text{C-1})$$

Table C-1 contains a summary of all the SA533 creep rupture tests reported by INEL. Included in this table are the temperature of the test (°F), the applied stress (ksi), the creep rupture time (hours), and the LMP. Figure C-1 shows the relationship between the applied stress and the LMP with the data points from the experiment. These data were fit with Equation (C-2), and the constants a and b were determined using a standard automated least squares fit utility as follows:

$$\text{LMP} = b \ln \frac{\sigma_{oe}}{a}; \quad (\text{C-2})$$

$$a = 5,272.0, \quad b = -5.7241$$

where σ_{oe} is the initial stress (ksi) given in Equation (B-11).

C.2 Larson-Miller Parameter Damage Index

Let a stress σ and a temperature T be applied for a time duration Δt . Then incremental damage is defined as follows:

$$\Delta D = \Delta t / t_r (\sigma, T)$$

where t_r is the rupture time determined from a constant-load creep-rupture test conducted for the stress σ and the temperature T . By rearranging Equation (C-1), the time-to-rupture t_r can be determined by Equation (C-4), where T is the temperature in Rankine,

$$t_r = e^{2.303[(\text{LMP}/.001T) - 20.0]} \quad (\text{C-4})$$

The total damage at any given time is given by the sum of the incremental damages as shown in Equation (C-5). Thus, failure by creep rupture is indicated when $D \geq 1.0$.

$$D = \sum \Delta t / t_r$$

Table C-1. Summary of the SA533B1 material creep tests

Temperature °F (°C)	Applied stress (KSI) (MPa)	Time to rupture (hours)	Larson-Miller parameter
1160 (627)	10.1 (70)	190.	36.08
1160 (627)	20.0 (140)	11.3	34.10
1340 (727)	5.7 (39)	8.9	37.70
1340 (727)	8.1 (56)	4.6	37.19
1430 (777)	2.0 (14)	264.	42.37
1430 (777)	3.8 (26)	18.9	40.21
1610 (877)	1.8 (12.5)	54.7	44.99
1610 (877)	3.8 (26.5)	4.1	42.66
1790 (977)	1.2 (8.0)	61.2	49.01
1790 (977)	1.8 (12.5)	2.2	45.76
1790 (977)	3.8 (26.5)	.045	41.96
2012 (1100)	0.5 (3.5)	46.9	53.56
2012 (1100)	1.0 (7.0)	0.65	48.97

The Damage Index D is calculated for each integration point in the grid and stored as a state variable for post processing.

$$\sigma_e = \frac{3}{2} S_1 \left(\frac{2}{3} \frac{\bar{\sigma}}{S_1} \right)^a e^{b \left(\frac{I_1}{S_2} - 1 \right)} \quad (C-6)$$

As noted previously, the LMP was determined from uniaxial constant-load creep data. Generalization of this data to multiaxial stress states through the effective stress σ_{oe} , given in Equation (B-11), would pose some difficulties because the definition for the effective stress does not distinguish between tension and compression. Similarly, the maximum principle stress cannot be used because it changes directions with time. Huddleston addressed this problem in two articles [C-3, C-4] where he developed a multiaxial creep-rupture strength model that showed good agreement with multiaxial creep-rupture data. The Huddleston model uses a new definition for the effective stress as follows:

where

$$S_1 = \sigma_1 - I_1$$

$$S_2 = \left(\sigma_1^2 + \sigma_2^2 + \sigma_3^2 \right)^{1/2}$$

$$I_1 = \sigma_1 + \sigma_2 + \sigma_3$$

$$\bar{\sigma} = \left[\left((\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 / 2 \right) \right]^{1/2}$$

Figure C-1. Stress dependence on Larson-Miller parameter

$\sigma_1 > \sigma_2 > \sigma_3$: The principal stresses

a, b: constants

Huddleston found that "a" tends to have a value in the range of 0.85 to 1.1 with 1.0 being a good universal value to use in the absence of data. The parameter "b" tends to have a value in the 0.15 to 0.3 range and an average value of 0.24 is recommended. Using these two values of a and b, Equation (C-6) reduces to

$$\sigma_e = \sigma_{oe} e^{0.24 \left(\frac{I_1}{S_2} - 1 \right)} \quad (C-7)$$

Where σ_{oe} from Equation (B-11) is substituted for $\bar{\sigma}$. This definition of the effective stress is used in the LMP expression, Equation (C-2), in place of σ_{oe} .

C.3 Consideration of Creep Ductility

As discussed previously, the measured creep elongation is strongly affected by localized necking which produces large data scatter. In a constant-stress creep test, the instability strain which is the onset of necking is generally a more reliable measure of material failure than the elongation. However, since the present data are based on constant-load tests it is not possible to recover the true instability strain from the data (the onset of tertiary creep is due to area reduction not material instability). It is therefore necessary to develop a measure of creep ductility from the constant-load data to be used as a confirmation of the validity of using the LMP in a life-fraction creep rupture model. In other words, the condition that $D \geq 1$ is only a necessary but not a sufficient condition for failure. This damage-based criterion need to be tightened by introducing the creep ductility-exhaustion criterion developed below.

Creep ductility under multiaxial stress conditions differs from and is significantly lower than the uniaxial elongation. The Mark-I shell is primarily under a biaxial stress state, and therefore the rupture strain is expected to be smaller than the rupture strain in a uniaxial test under equal effective stress. In order to develop the biaxiality dependence of the creep ductility, we make use of work by Manjoine [C-5]. Manjoine cited tests on various ductile materials and proposed the following formula for the ductility ratio μ :

$$\mu = \frac{\epsilon_{eff}}{(\text{uniaxial elongation})} \quad (C-8)$$

$$\mu = 2^{(1-TF)} \quad (C-9)$$

Where TF is the Davis triaxiality factor.

$$TF = \frac{\sqrt{2} (\sigma_1 + \sigma_2 + \sigma_3)}{\left[(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 \right]^{1/2}} \quad (C-10)$$

A literature survey (e.g., [C-7], [C-8], [C-9]) gives additional experimental data supporting the plate fracture phenomenon under biaxial states of stress similar to the conditions existing in containment liners. The ductility relationship, Equation (C-9), has been found to be a reliable formulation, particularly for the triaxiality factors which are most likely to occur in containment liners. While Manjoine proposed Equation (C-9) for the range $0 < TF < 5$, we need only be concerned with $1 < TF < 2$. Since the out-of-plane stress (σ_3) has a negligible effect on TF, Equation (C-10) reduce to

$$TF = \frac{(\sigma_1 + \sigma_2)}{\left(\sigma_1^2 - \sigma_1 \sigma_2 + \sigma_2^2 \right)^{1/2}} \quad (C-11)$$

With this formula, it is straightforward to calculate ductility ratios at various liner stress states. For example, for uniaxial tension, $\sigma_2 = 0$, $TF = 1$, ductility ratio = 1. For $\sigma_1 = \sigma_2$, the approximate stress condition in the shell, $TF = 2$, ductility ratio = 0.5, which means that the liner will tear at an effective plastic strain of only half the uniaxial failure strain. This range of application of Equation (C-9) is plotted in Figure C-2 with additional biaxial stress data added from the literature survey already cited. The reciprocal of the Ductility Ratio is used in the present analysis to magnify the calculated effective strain before it is compared to the uniaxial creep elongation. This has been labeled the biaxiality coefficient (β), such that

$$\epsilon_r = \beta \epsilon_{eff}, \quad (C-12)$$

$$\beta = \frac{1}{\text{ductility ratio}} = \frac{1}{\mu}, \quad 1.0 \leq \beta \leq 2.0 \quad (C-13)$$

where ϵ_r is the strain to be compared with the uniaxial elongation data.

Uniaxial constant stress temperature experiments conducted by INEL [C-2] exhibited specimen elongations ranging from 20 to 80 percent, which is indicative of the wider scatter exhibited by the elongation data.

An alternative procedure for deriving a relationship for the uniaxial rupture strain can be developed using the LMP and rupture time from constant-stress tests compared to constant-load tests rupture times. Comparing the rupture time for limited constant-stress data [D. Sturm, University of Stuttgart, private communication, May 25, 1992], it was observed that the σ vs LMP curve shown in Figure C-1, which was developed for constant load tests, can be shifted to the right to obtain a curve for constant-stress tests. This shift was estimated to be

$$\Delta LMP = 0.001T \quad (C-14)$$

This equation, Equation (C-2) for LMP vs σ , and the relationship between the initial stress and the final stress at rupture, namely,

$$\sigma_o = \sigma e^{-\epsilon_r} \quad (C-15)$$

where

σ_o = the initial stress

σ = the final stress at rupture

ϵ_r = the rupture strain that is consistent with the LMP

are used to arrive at an expression for the rupture strain that is consistent with the LMP as follows

$$\epsilon_r = 1.747 \times 10^{-4} T \quad (C-16)$$

where T is in degrees Rankine. Equation (C-16) is shown in Figure C-3 compared to elongation data, converted to logarithmic strains, from Reference C-2. As can be seen, Equation (C-16) gives a reasonably good correlation with the data and the least squares fit of the data. The least squares fit of the data has a standard deviation, σ , of 13.1 percent strain and a correlation, r , of 0.43. Thus, strains that are within 13 percent of the rupture strain predicted from Equation (C-16) are considered failure strains within the bounds of uncertainty.

Using this expression for rupture strain and a biaxiality coefficient $\beta = 2$, we state the creep ductility-exhaustion criterion as follows:

$$\epsilon_{eff} \geq 0.874 \times 10^{-4} T \quad (C-17)$$

Figure C-2. Reduced version of Manjoine's multiaxial failure criteria related to containment liners

Figure C-3. Creep rupture ductility developed from the Larson-Miller parameter data for SA533 B1 steel

Appendix C

Condition C-17 and C-5, satisfied simultaneously within the bounds of uncertainty, are necessary and sufficient conditions for shell rupture. This double criterion was used in the present analysis to predict the creep rupture temperatures. Equation (C-16) can be implemented as the denominator in an incremental damage formulation similar to Equation (C-5), namely

$$D = \sum \Delta \epsilon_{\text{eff}} / (\epsilon_r / \beta) \geq 1 \quad (\text{C-18})$$

For monotonic loading, Equations (C-17) and (C-18) give equivalent results.

C.4 References

- C-1 Larson, F. R., and Miller, J., "A Time-Temperature Relationship for Rupture and Creep Stresses," Transaction of the ASME, July 1952, pp. 756-775.
- C-2 Rempe, J. L., Thinnies, G. L., and Allison, C. M., Light Water Reactor Lower Head Failure Analysis, U.S. Nuclear Regulatory Commission, NUREG/CR-5642, 1990.
- C-3 Dameron, R. A., Dunham, R. S., Rashid Y. R., "Conclusions of the EPRI Concrete Containment Research Program," Nuclear Engineering and Design, #125 V, 1991, pp. 41-55.
- C-4 Huddleston, R. L., "An Improved Multiaxial Creep-Rupture Strength Criterion," Journal of Pressure Vessel Technology, Vol. 107, November 1985.
- C-5 Huddleston, R. L., "Assessment of an Improved Multiaxial Strength Theory Based on Creep-Rupture Data for Type 316 Stainless Steel," ASME Pressure Vessel and Piping Conference, New Orleans, LA, June 22-26, 1992.
- C-6 Manjoine, M. J., "Creep-Rupture Behavior of Weldments, Welding Research Supplement," February 1982.
- C-7 Dictor, G. E., Ductility, American Society for Metals, October 1967.
- C-8 Aerospace Structure Metals Handbook, Vol. 2, 1981.
- C-9 Eibl, J., Schluter, F. H., and Terbeck, G., Ultimate Strains of Liner Sheets Under Biaxial Tension, Test Report from Massivbau Baustofftechnologie Karlsruhe, 1988.

APPENDIX I

**WORKING GROUP MEMBERS' COMMENTS
ON THE INDEPENDENT EVALUATION**

Appendix I

Working Group Members' Comments on the Independent Evaluation

Appendix I is a compilation of respective working group members' comments on the independent evaluation of four major issues discussed in Parts II through V of the report. The author's responses to the comments were incorporated into the respective parts of the report. For the purpose of referencing the comments, the following identifications apply.

<u>Working Group</u>	<u>Topic</u>	<u>Test Reference</u>	<u>Group Members</u>	<u>Comments Provided</u>
1	Liner Failure Criteria	Part V	K. Bandyopadhyay R. Henry D. Horacek Y. Rashid P. Shewmon	Yes Yes No N/A* Yes
2	CORCON Audit Calculations	Part III	D. Bradley R. Henry D. Powers B. Spencer	N/A* Yes N/A* Yes
3	Melt Spreading	Part IV	F. Moody M. Podowski D. Powers B. Spencer	Yes Yes Yes N/A*
4	Melt Release Conditions	Part II	V. Denny R. Henry S. Hodge M. Podowski R. Schmidt	Yes No Yes** N/A Yes

* Not applicable since these individuals were involved in the independent evaluation.

** Comments were provided during the early phase of the work.

During the review process, authors and reviewers often exchanged information and discussion on the respective issues which led to one or more revisions of the reports.

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FACSIMILE COVER SHEET

ENGINEERING RESEARCH & APPLICATIONS DIVISION

FAX NO.: (516) 282-4255

BUILDING 475C

Department of Nuclear Energy
Brookhaven National Laboratory
Upton, NY 11973

DATE: December 23, 1992
Revised February 8, 1993

TO: Sudhamay Basu

FACSIMILE TELEPHONE NO.: 301-492-3585

FROM: Kamal Bandyopadhyay

TELEPHONE NO: (516) 282-2032

MESSAGE: REVIEW OF ANATECH REPORT ON CREEP-RUPTURE FAILURE IN A MARK I
CONTAINMENT REPORT NO. ANA-92-0143, NOVEMBER 1992

It appears that most of the information presented in graphical forms was by use of a color code which does not appear in the reproduced copy I reviewed. The information presented in tabular forms is often incomplete (e.g., spatial distribution of elements is missing in Table 3-1). Therefore, it was very difficult to properly review the analysis modeling and results presented in the report in its current form. Anyway, I would provide the following comments based on the information I managed to get out of the report.

KEY ISSUE

The most important information that sets the stage for major conclusions regarding the failure mechanisms is embodied in Appendix A, particularly Figure A-1. The material loses most of its strength being heated up from about 1000°F to 1500°F. By 1600°F, it has lost more than 90% of its strength and stiffness. For temperatures greater than 1600°F, the uncertainties of material properties (e.g., yield stress, modules of elasticity and creep strain) are extremely high.

The structural analysis (including finite element model, computer code, constitutive relations and damage estimation) was performed by extremely competent people such as Dr. Joe Rashid following the state-of-the-art knowledge in this field. The validity of the analysis technique was demonstrated in the past through tests and analyses. However, such validation was accomplished for low material temperatures at least below the phase change temperature.

No justification was provided as to why the same damage estimate will be applicable at high temperatures. For example, an effective strain value of 10%-20% can be considered to be the failure limit for steels at reasonable temperatures (e.g., up to 1000°F); however, it is not clear why at very high temperatures (e.g., 2300°F) a larger strain cannot be accommodated.

SIMPLIFIED MODEL

The sharp drop of the material strength and stiffness values clearly indicates that the failure will be highly localized along the corium-shell contact surface (like a tear drop). The remaining colder region of the shell will act as a support for the hotter surface. In other words, the material information alone is sufficient to reduce the analysis model to a simple plate encompassing the corium-shell contact area, or even a beam spanning in the vertical direction since the contact length in the circumferential direction is much greater (see sketch).

For example, for a maximum radial displacement of $\ell = 0.82"$, $R = 2.85"$, $\alpha = 0.7784$, $C = 4.44"$, ϵ (strain) = 11%.

The strain computed in this region in the report is 10-18%. Therefore, a reasonable correlation exists.

If the shell were to touch the concrete, $\epsilon = 57\%$. Therefore, the question is whether such a strain can be accommodated for the highly visco-elastic

material. Forging of carbon steel is conducted at such high temperatures and experience shows that in this process such a large deformation may be easily accommodated (References 1, 2 and 3).

$$h = 2\ell, R = \frac{\ell^2 + h^2}{2\ell}$$

$$\alpha = \sin^{-1} \frac{h}{R}$$

$$C = 2R\alpha$$

$$\epsilon = \frac{C - 2h}{2h}$$

CONCLUSIONS

The analysis performed by ANATECH focused on refining the model and following the constitute relationship that was developed at low temperatures. Dr. Rashid and his associates have a record of "fine tuning" such models and achieving reliable results at low temperatures. However, for the present study, the model could have been simplified without losing essential characteristics as shown above and the effort should have concentrated on establishing a revised acceptable strain range for estimating the damage/failure of the shell. The difficulties of obtaining defensible material failure strain data at such high temperatures are recognized. But without such information, the postulation of a creep strain failure instead of achieving a contact with concrete and an eventual melt-down is not properly supported. The information on forging available with the steel mills may be useful for this purpose.

REFERENCES

1. Camp, J.M. and C.B. Francis, "The Making, Shaping and Treating of Steel," Fifth Edition, Carnegie-Illinois Steel Corporation, Chicago, Illinois, 1940.
2. ASM International Handbook Committee, "Metals Handbook Ninth Edition," Volume 14, Forming and Forging, Metals Park, Ohio 44073.
3. Smithells, Colin J., "Metals Reference Book," Volume III, Fourth Edition. Plenum Press, New York, New York, 1967.

Fauske & Associates, Inc.

December 23, 1992

Dr. Sud Basu
U.S. Nuclear Regulatory Commission
Washington, D.C. 20555

Dear Sud:

I have reviewed two of the three audit calculations for the response of the Mark I liner under severe accident conditions, as represented in NUREG/CR-5423. These have been reviewed in the context of their application to the decision making process, specifically with respect to the questions to be addressed by such audit calculations.

1. Is the analysis reported in the NUREG/CR-5423 reasonable with respect to the issues addressed in the report?
2. Does NUREG/CR-5423 adequately represent the uncertainties that should be addressed based on the analyses reported in the audit document?

The conclusions of the audit documents need to be such that these questions are addressed.

I have reviewed the audit calculations with CORCON-MOD3 and the structural analyses presented in the ANATECH report. My assessment of each of these is given below.

CORCON Audit Calculations

The analyses performed with CORCON-MOD3 appear to be performed in a manner consistent with that necessary to establish the robustness of the analyses reported in NUREG/CR-5423. Also, the results are reported in a manner consistent with an audit calculation.

ANATECH Analyses

From my review of the ANATECH audit document, it is apparent that the necessary information is available but it is not reported as an audit calculation.

With respect to the first question, the three-dimensional analyses in the audit report show conclusively that the liner failure would occur at temperatures less than the steel melting temperature. However, the calculated failure temperatures are still quite high. Values typical of those recommended by the 3D analysis have already been incorporated into the Mark I analyses. Therefore, the answer to the first question is yes.

For the second question, one needs to consider the uncertainties associated with the containment liner strength. These include the debris depth, the extent of the periphery (angle) of the drywell wall raised to the elevated temperatures addressed in the analysis, the temperature gradient through the liner, etc. The audit report currently uses a two-dimensional approach (360° contact) to represent the lower bound of the temperatures at which failure could be prevented. Unquestionably this calculation provides an answer which is a lower bound to the temperature at which the containment integrity would not be challenged. On the other hand, one must also ask if the analysis addresses the physical conditions of interest for the issue in question. Since the particular point in question is one of the three-dimensional response of the containment liner, and particularly since only one relatively small angular region would be at the elevated temperatures if water was present, the two-dimensional analysis does not address the specific issue of interest for the Mark I liner attack. Consequently, this analysis is not applicable as an audit calculation for the analyses performed in NUREG/CR-5423. As a result, I suggest that the two-dimensional calculations in the ANATECH report be set aside as interesting, but not applicable to the issue of interest. The focus for the ANATECH document should be the three-dimensional calculations performed.

It is also my assessment that the 3D calculations represented in the ANATECH document are conservative for at least two reasons.

1. The temperature profiles represented in NUREG/CR-5423 are indicative of a heat transfer boundary condition in which the debris remains in contact with the steel liner. For the large displacements (strain) presented in the ANATECH document, one would expect the liner to separate from the debris crust and thereby add another heat transfer resistance between the molten debris and the wall. This could enable the wall to increase in temperature at a slower rate or to cool somewhat in this sensitized locale. Consequently, the large localized growth calculated in the ANATECH analyses is a conservative representation of the actual behavior.
2. The typical values used for strain-to-failure in the analysis are representative of those values usually measured at comparatively low temperatures. As the material loses some strength at elevated temperatures, one would also anticipate that it becomes more ductile and therefore would have greater values for the strain-to-failure. In this respect the 3D analysis presented in the ANATECH report are also conservative.

For the above reasons, I believe that the information in the ANATECH document provides the necessary information for the audit calculations needed by the NRC for closure on the Mark I liner issue. However, the two-dimensional calculation is not appropriate as an audit calculation and should be set aside. The three-dimensional calculation is appropriate, conservative and satisfies the necessary objective.

December 23, 1972

I apologize that I have not been able to provide a review of the third document. Time did not allow for a sufficiently in-depth review and therefore I would prefer to defer my judgement to those who have already reviewed this report for the NRC. If I can be of any additional service in this matter, please feel free to give me a call.

Sincerely yours,

Robert E. Henry, Senior Vice President
Process Safety Department

REH:jab

 TO: DR. SUD BASU FROM: DR. PAUL SHEWTON
 NRC, RES/AEB ACRS, COLUMBUS OH
 Tel: 493-3561 Tel: 614-457-4378
 FAX 301-492-3585 FAX: 614-457-2995

Dear Dr. Basu,

COMMENTS:

2. I was interested in the failure/rupture criteria used, and they seem appropriate. Long time creep strains to failure (1000+ hour tests) can be quite low (a few percent), but the assumed value of 10-20% in a uniaxial test is reasonable, and the creep test results they give, TABLE C-1, are for appropriate times (10-100 hr), or close enough.

ARGONNE NATIONAL LABORATORY

9700 South Cass Avenue, Argonne, Illinois 60439

October 23, 1992

Dr. S. Basu
U.S. Nuclear Regulatory Commission
5650 Nicholson Lane
Rockville, MD 20852

Dear Dr. Basu:

I have reviewed the CORCON audit calculations of melt superheat duration for MCCI performed in support of NUREG/CR-5423 analyses. The audit calculations are contained in SAND91-2692. The conclusion from the audit calculations is that the duration of superheat predicted with CORCON-MOD3 is shorter than was used in the analyses in NUREG/CR-5423. The superheat definition is the conventional one; that is the superheat is the incremental temperature of the melt above its liquidus temperature.

The audit calculations are a very well focused examination of the superheat duration for a spectrum of code cases as calculated by CORCON-MOD3. The results are very clearcut and unambiguous and confirm that the CORCON code calculates a consistently shorter superheat duration. Hence, the analyses in the NUREG report regarding the heat transfer from the melt to the steel wall can be regarded as conservative.

Yours truly,

Bruce W. Spencer
Reactor Engineering Division
Engineering Development Laboratories

BWS:ljo

Dr. Sud Basu
Accident Evaluation Branch
Division of Systems Research
Office of Nuclear Regulatory Research
Washington, DC 20555

February 1, 1993

Dear Sud,

I have reviewed the revised draft of the report, "MELTSPREAD-1 Calculations of Corium Depth Next to the Mark I Liner with NUREG/CR-5423, by Sienicki, Chu, and Spencer.

The purpose of this study was to compare rigorously calculated corium depths on the liner with Causal Relation One (CR1) predictions from Theofanous' study presented in, "The Probability of Liner Failure in a Mark-I Containment," NUREG/CR-5423.

The subject report appears to have addressed all of the major concerns expressed in previous reviews, incorporating ranges on important parameters. The calculated results are shown to be bounded by the corium depths of CR1, except for a release of 9 cubic meters, which slightly exceeds the 95 percentile bound.

The assumptions, models, and parameter ranges are described sufficiently to give confidence that the results are moderately conservative, and would not be exceeded by a significant amount in a full size accident. Figures 4.5 through 4.7 provide evidence of the modeling conservatism.

It is my opinion that this study is mature, and possible future refinements will not materially affect the predicted results. Further predictive work is unnecessary. The results and conclusions of NUREG/CR-5423 are strongly supported by the MELTSPREAD-1 study.

Thank you for the opportunity to review this work and comment on it.

Yours truly,

Fred Moody

Fred Moody
GE Nuclear Energy
Mail Code 469
175 Curtner Avenue
San Jose, CA 95125
(408) 925-6434

cc: J. J. Sienicki, ANL
T. G. Theofanous, UCSB

Rensselaer

Department of Nuclear Engineering & Engineering Physics

January 25, 1993

Dr. Sud Basu
Nuclear Evaluation Branch
Division of Systems Research
Office of Nuclear Regulatory research
U.S. Nuclear Regulatory Commission
Washington, D.C. 20555

Dear Dr. Basu:

Enclosed are my comments on the report entitled "Comparison of MELTSPREAD-1 Calculations of Corium Depth Next to the Mark-I Liner with NUREG/CR-5423", by J.J. Sienicki, C.C. Chu and B.W. Spencer. In addition, I am sending you a copy of the paper on a similar subject based on a combined experimental/analytical work at RPI. As I pointed out in my review, the best way to obtain reliable estimates of the consequences of melt spreading and Mark-I liner attack is via comparing and cross-examining the results of various analyses and experimental/numerical simulations. This type of work was the subject of the attached paper. Its main objective was to investigate fundamental physical phenomena rather than perform a specific study such as that in the report which I reviewed. Nevertheless, even the results already shown in the paper can be used to complement those of Sienicki et al., and additional parametric studies can add a substantial amount of new information important for the liner failure issue resolution.

Please, feel free to contact me to discuss any comments or questions which you may have in regard to the matter of this letter.

Sincerely yours,

A handwritten signature in cursive script, reading "Michael Z. Podowski".

Michael Z. Podowski
Professor

cc. F. Eltawila

Comments on the report entitled "Comparison of MELTSPREAD-1 Calculations of Corium Depth Next to the Mark-I Liner with NUREG/CR-5423", by J.J. Sienicki, C.C. Chu and B.W. Spencer.

General Comments.

The reviewed report is concerned with a parametric study of melt spreading on the floor of Peach Bottom 1 & 2 Mark-I containments during hypothetical core meltdown accidents. The analysis was performed using the MELTSPREAD-1 computer code. The situations analyzed were based on the Scenario-I and Scenario-II conditions proposed in the probabilistic study on Mark-I containment failure issue by Theofanous et al. (NUREG/CR-5423).

Several cases have been investigated by the authors, including the effects of corium composition, heat loss from the melt, phase change temperatures, melt superheat, and others. The results of calculations shown in the report indicate that the melt thickness near the liner predicted by MELTSPREAD-1 lies within, or close to, the bounds used in NUREG/CR-5423. In this sense, the present report clearly confirms the assumptions made by Theofanous et al. Naturally, the correctness of the overall analysis is contingent upon two major factors: the completeness and consistency of the present study and the accuracy and correctness of the modeling assumptions and computational methodology of MELTSPREAD-1. In regard to the former issue, the authors analyzed the sensitivity of calculations to several assumptions. Whereas the scope of the study is quite extensive and it seems to be free of any major errors, there are certain questions which could, and should, be clarified and/or supported by additional evidence. Specific comments in this regard are given below. The resolution of the latter issue involves the validation of MELTSPREAD-1 against experimental data, analyses of corium spreading and liner attack phenomena using other models and physical simulations etc. Since this issue goes beyond the scope of the report by Sienicki et al., it will not be discussed in this review.

Specific Comments.

In regard to Table 1.1.

1. The transition from laminar to turbulent flow conditions in free-surface flows occurs at much lower Reynolds numbers than for flows in pipes. As a result, turbulent flows are likely during melt spreading for high release rates. It is not clear

if such flow conditions, and their effect on corium-concrete heat transfer, are accounted for in MELTSPREAD-1.

2. The formation of crust at the upper surface of the melt may significantly reduce the heat loss to water above. This heat loss is very sensitive to the surface temperature (radiative heat transfer is proportional to T^4). It would be useful if stand-alone scoping calculations were shown to confirm that the proposed 50% reduction in the heat transfer coefficient is sufficient.

3. The statement on p.1-7 that the "... Spreading behavior in the present analysis is not expected to be sensitive to rate or extent of chemical reactions..." is not clear. For instance, oxidation of Zr in contact with steam from concrete ablation may significantly heat-up the corium and, therefore, affect the spreading distance on the containment floor.

4. The non-Newtonian behavior of molten corium may have two effects opposing each other. On the one hand, it will (as stated in the report) increase the resistance to flow and enhance corium freezing from the bottom. On the other hand, however, the solidified layer will insulate the corium from the concrete floor and reduce gas generation, thus facilitating the flow of subsequently delivered corium. The effect of this phenomenon on MELTSPREAD-1 predictions should be discussed.

5. It is known that the liquefaction temperature of eutectics may be lower than the melting temperature of the interacting materials. Among others, a study in this regard was performed at KfK (Karlsruhe, Germany). In the present calculations the lowest temperature of U-Zr-O eutectic was assumed at 1900°C (which is higher than the melting temperature of metallic Zr). It is not clear if the authors were aware of the above-mentioned experimental results.

6. Before any computer code is applied to solve particular problems, its numerical consistency should be fully tested, acceptable accuracy firmly shown, and error bounds assessed. If it was the case in regard to the MELTSPREAD-1 code, the effects of "Eulerian mixing" and non-conservative form of equations must have been discussed and quantified when the core was released. Such factors should not be treated as a source of uncertainty in the present study.

Other comments.

7. In Table 4.1, the only difference between cases I4 and I16 is the release melt superheat. Although the I4 superheat is only 12°K, compared to 37°K for I16, the spreading extents and maximum corium depths are practically the same. Is it really true that melt superheat does not affect spreading?

8. A similar, probably even more significant, question can be raised in regard to Table 4.2. The superheat in case IIP2 is 87°C, the Zr content is 30% and the resultant spreading distance is larger than in case IIP5 where the superheat is 100°K and the Zr content is 50%. At the same time, comparing cases IIP2 and IIP6 indicates that higher Zr content results in a longer spreading distance, as expected.

9. Along the same lines, why does increasing the melt superheat from 87°K to 150°K, combined with reduced heat losses from the melt, (cases IIA1 and IIA2 in Table 4.3) reduces the spreading distance from node 8 to node 5?

10. The results in Figs. 4.2 through 4.4 should be explained. Specifically, the reasons why the height of immobilized material either stays constant or suddenly changes.

Michael Z. Podowski

Michael Z. Podowski

4 of 4

January 31, 1993

Dr. Sud Basu
Division of Systems Research
Office of Nuclear Regulatory Research
USNRC Mailstop NL/N 344
Washington, DC 20555

Dear Dr. Basu:

Enclosed please find review comments for the RPI report entitled "The Effect of Heatup and Meltdown of Reactor Vessel Upper Internals on the Composition of Corium Discharged into the Drywell of BWR MARK-I Containments", authored by Professor Podowski, with assistance by Chang S. Cho. This work, which was sponsored by ORNL, provides extensive parametric calculations for the timing and composition of the discharged corium, with principle emphasis on heat-up and meltdown behavior following ADS operation for short-term station blackout. (The election of [early] depressurization of the vessel results in near adiabatic heatup of the fully-uncovered core for a significant time interval followed, thereafter, by substantial rates of thermal radiation from the upper surface of the presumed core rubble bed to overheating and liquefying upper vessel internals.)

Based on their generated results, the authors conclude that the initial corium release to containment, which is predicted by the APRIL.MOD3 code to result from early (order 3 hours) local failure of the lower head drain plug, contains very little molten urania (less than 10 per cent by mass). For the base case, the total initial release (10-15 minute time period) was predicted to be 35,000 kg (about 0.5 cubic meters per minute) consisting primarily of molten steel from the upper vessel; thereafter, a much lower discharge rate of increasingly higher urania content is predicted during the subsequent 3 hour period. The latter "discharge as melt" interval is driven by decay heat only and gives, for the base case (bottom peaked power distribution, no CRD flow, 0.5 emissivity for thermal radiation, vessel failure at 173 minutes), a large cumulative mass in containment with the end state masses for steel, Zircaloy, and urania totaling about 100, 35, and 25 tons, respectively. This dominance by the metallic components is some-

what diminished if vessel failure is delayed; for a one hour delay (failure at approximately four hours), the steel mass is reduced to about 60 tons, due primarily to the larger down-side effects of the lower bound assumption for failure state (completely solid debris in the lower head at failure)--recall author's Figure VI-6.

Based on reviewer's experience with the EPRI-developed CORMLT code, wherein a similar thermal radiation network for a PWR upper plenum

also predicts melting of upper internals (albeit for a shorter period), the APRIL predictions for steel melting appear reasonable. Moreover, such synergistic effects as steam oxidation of molten steel and natural circulation heating could give even higher rates. Nonetheless, reviewer is not persuaded, despite the large steel mass involved, that oxidic compositions are necessarily low. Several effects, all of which are difficult to quantify, promote larger oxidic content. These include:

1. Erosion of ceramic fuel by liquefied steel. Two effects come to mind. First, the large temperature difference between in-core material and relocating steel may induce spallation/entrainment of fuel particulates. Second, eutectic interactions between steel and Zircaloy, with augmentation of in-core melt volumes, may accelerate fuel dissolution by zirconium. Both effects might increase the oxidic fraction in the bottom head prior to vessel failure. (Reviewer is unaware of any careful work on the rate processes involved.)
2. Oxidation of Zircaloy by intermittent steam bursts attending quench of relocating steel in the bottom head. (For the low pressure situation involved, it is reviewer's experience that nearly-complete burning of in situ Zircaloy is a possibility.) The water mass in the bottom head (approximately 30 tons) is sufficient to oxidize about 80 tons of Zircaloy; a large uncertainty is the oxidation of relocating molten Zircaloy by counterflowing steam; very little understanding exists of the dynamics involved, in particular at prototypic scale. (The necessary time window appears to be established--compare Figures I-2 and I-5.)

In summary, the work performed by the RPI team provides added perspective on the initial conditions for the MARK-I liner melt-through problem. Although residual issues remain, the use of an early-stage metallic-rich discharge is favored by the available information, including the contribution by BWRSAR.

If I can be of further assistance to closure of this matter, please give me a call at my home or office:

Home: (408) 353-2538

Office: (415) 960-5930 or (310) 781-8728

781-9022

Sincerely,

A handwritten signature in black ink, appearing to read "Vernon E. Denny", is written over the typed name. The signature is fluid and cursive, with a large loop at the end.

Vernon E. Denny

Chief Scientist
Science Applications International Corporation

cc.
T. Theofanous

Sandia National Laboratories

Albuquerque, New Mexico 87185

December 16, 1992

Dr. Sudhamay Basu
Accident Evaluation Branch
Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
MS: NL/N-344
Washington, DC 20555

Dear Dr. Basu:

Enclosed are my comments which you requested concerning the report entitled, "The Effect of Heatup and Meltdown of Reactor Vessel Upper Internals on the Composition of Corium Discharged into the Drywell of BWR MARK-I Containment", by M. Z. Podowski, and C. S. Cho. As you also requested, I will send a copy of these comments to Dr. Podowski.

If additional information is needed, please contact me (tel. 505-845-3057).

Sincerely,

Rodney C. Schmidt
Reactor Safety Experiments Division 6423

RCS:6423

Copies:

USNRC	R. W. Wright
6423	K.O. Reil
6423	R. O. Gauntt

R. Schmidt's Brief Review Comments on
"The Effect of Heatup and Meltdown of Reactor Vessel Upper Internals on the Composition of
Corium Discharged into the Drywell of BWR MARK-I Containment",
by M. Z. Podowski, and C. S. Cho.

Background:

On January 10, 1991, I participated in a USNRC sponsored working group meeting convened to look at the initial release melt quantities and compositions used in the BWR MARK-I liner failure study of Theofaneous et al. (NUREG/CR-5423). A summary of the working group discussion was given in a memo from F. Eltawila to Dr. B. Sheron dated May 22, 1991. The following paragraph can be found on page 7:

"Regarding melt composition, a significant amount of time was devoted to discussing mechanisms that might yield a significantly higher content of steel in a Scenario I-type release. The implication of such behavior would be a significantly higher effective superheat since the steel would be in appropriate thermal equilibrium with the metallic component, and such superheat could be "felt" in the appropriate thermal loading of the liner if the steel quantities were indeed large enough. The origins of these concerns appear to lie in PWR high pressure scenario calculations where natural convection between the core and upper internals could bring about significant melting of the upper internals steel. There was some speculation that even under the low pressure scenarios of interest here, radiation heat transfer might be sufficient to bring about a similar behavior. It was pointed out that, due to axial power shape, the upper portion of the core would present an effective thermal resistance against such heat transfer, but this issue could not be settled at the meeting. It was agreed to examine APRIL and other codes with reference to their capability to adequately represent these phenomena and to carry out computations with these codes as appropriate."

My understanding is that the report being reviewed here is a description of the analysis which was carried out using the APRIL-MOD3 code as a consequence of the above mentioned meeting. Having carefully read this report I do have a number of comments. However, although my reading of the report inevitably raised various questions about the technical details of the modeling, assumptions, etc., I don't believe that most of these comments/questions are particularly relevant to the main purpose of this review. Thus, my comments are brief and relate primarily to the overall conclusions.

Additionally (for reference), a Scenario-I type of situation is one where a robust in-core blockage occurs, a large in-core molten region develops, and then a massive pour finally occurs into the lower plenum, quickly failing the lower head. A Scenario-II type of situation is one where a robust in-core blockage does not form, the lower melting point metallic materials move to the lower plenum early on, core material relocation to the lower plenum occurs gradually, and material is initially quenched by the water pool in the lower plenum. Scenario-II types of scenarios were expected to yield a corium composition at RPV failure with a high metallic concentration, whereas a Scenario-I type of situation was expected to yield a highly oxidic composition.

Comments:

The major conclusion of the analysis (given in the executive summary) is that

"the APRIL-MOD3-predicted major characteristics of the melt during the phase of high release rates stay within the range of values used by Theofaneous et al. [1990]. Therefore, it can be concluded that, within the scope of this work, the bounding conditions used in the NUREG/CR-5423 report seem to be adequate"

Having read the report, I would agree with this conclusion. However, taken by itself, this conclusion does not convey the whole story. As indicated in the background section above, a

major point of discussion during the working group meeting was whether or not significant upper internal structure heating during Scenario-I types of accident sequences would lead to an increased metallic composition. As clearly pointed out in the body of this report, and also in the conclusions, no Scenario-I type of events were predicted in this analysis, only Scenario II types of events were predicted. This is because the models used in APRIL do not lead to the formation of a robust blockage region in the core.

Thus, although I can agree with the conclusion as stated above, I cannot feel completely confident that we can, as yet, rule out the possibility that the heating of upper region internal structures in a Scenario-I type of accident would lead to melt releases containing comparable amounts of metallic and oxidic components. In other words, because of the way the analysis turned out (i.e., no Scenario-I events predicted), the results do not really address the question which I feel was actually being asked by the working group. I also cannot agree with the statement made on page 32, under the section "Final Conclusions", the second paragraph, which implies that Scenario I types of accident scenarios "can be prevented by timely depressurizing the reactor system." In my judgement, we do not yet have the technical basis to know whether or not a low pressure dry core BWR accident (such as a short term station blackout with ADS) will lead to a Scenario I type situation or a Scenario II type of situation.

In closing, although my current personal opinion is that significant heating and melting of above-core structures would probably not occur in Scenario-I types of situations (at least to the extent that the eventual metallic composition of the melt pour would become comparable in mass to the oxidic composition), I don't think that we, as yet, have a firm technical basis from which to completely rule out this possibility.

Sandia National Laboratories

Albuquerque, New Mexico 87185

February 25, 1993

Dr. Sudhamay Basu
Accident Evaluation Branch
Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
MS: NL/N-344
Washington, DC 20555

Dear Dr. Basu:

As requested, I have reviewed the revised conclusions section of the report, "The Effect of Heatup and Meltdown of Reactor Vessel Upper Internals on the Composition of Corium Discharged into the Drywell of BWR MARK-I Containment", by M. Z. Podowski, and C. S. Cho. The following comments are intended as a followup to the initial review comments sent to you on December 16, 1992.

As expressed in my initial review of the report, I feel that this is a good report, that it should be published, and that I basically agree with the major conclusions. However, I did take issue with one statement in the original conclusion section that had to do with preventing Scenario 1 type of accident scenarios by timely depressurization. The key change I was looking for was the distinction between results as predicted by the APRIL code (which do indicate that timely depressurization will prevent Scenario 1 type events) and the overall technical judgement of the reactor safety community, which in my view, is still not certain whether or not a low pressure dry core BWR accident (such as a short term station blackout with ADS) will always lead to a Scenario II (core melt drainage directly to lower head) type of situation. In any case, this section has been rephrased and extensively rewritten and I am comfortable with what has now been written on this point.

The other area I previously brought up was the issue of whether or not this work answered a specific question raised by the NUREG/CR-5423 review working group focused on initial release melt quantities and composition. This question is based on a presumption that we do have a Scenario I kind of accident, and that given this situation, asks whether heat transfer to the upper internal structures would be sufficient to significantly increase the metallic composition of the melt due to melting of these upper structures. However, a key result of this study is that APRIL calculations do not lead to a Scenario I kind of accident under these conditions. This is an important and useful result. However, because of the specific nature of the above mentioned question, I expressed my opinion that the results do not directly apply to this particular question (Of course they do apply to the question of how likely we may feel a Scenario I type of accident

would be). Thus, in my judgement (both then and now), we must recognize that the aforementioned question basically remains an open one, and would require further investigation if additional insight is desired.

With respect to dealing with this open question, my personal opinion is the following. I believe it prudent to simply leave the question as an open one until after the results of the NRC sponsored XR series of experiments have been obtained. These experiments are intended to address the technical issues and physical processes which will determine whether a Scenario I or Scenario II type of accident is likely under BWR dry core accident conditions. If the predictions of APRIL (and other analysis such as the Oak Ridge BWRSAR calculations) are basically substantiated by the experimental results, then the compositional details of the Scenario I melt specified in NUREG/CR-5423 are not important since we will have determined that the possibility of ever getting into such an accident scenario is extremely remote. If on the other hand, this is not the case, then I believe it would be prudent to consider ways in which appropriate analysis could be carried out to further clarify this issue.

I hope that you will find these comments useful to you. I also want to express my appreciation for the opportunity to participate in the review. It has been an interesting and useful technical exercise for me.

Sincerely,

Rodney C. Schmidt
Reactor Safety Experiments Division 6423

RCS:6423

Copies:

USNRC	R. W. Wright
RPI	M. Z. Podowski
6423	K.O. Reil
6423	R. O. Gauntt

APPENDIX II

**EXPERT PEER PANEL REPORTS
AND AUTHOR'S RESPONSES**

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Fauske & Associates, Inc.

June 7, 199

Dr. Brian Sheron, Director
Divisions of Systems Research
Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
Washington, D.C. 20555-0001

Dear Brian:

In response to your letter of April 27, 1993 I have reviewed the draft report entitled, "The Probability of Mark-I Containment Failure by Melt-Attack of the Liner, (NUREG/CR-6025)". As you noted in the letter, this report is a sequel to NUREG/CR-5423 and includes the resolution of the four specific issues defined at the Harpers Ferry meeting in July of 1990. You ask my expert opinion on:

- (a) whether the four individual issues have been sufficiently resolved given the state of our current knowledge, and
- (b) whether the results of the resolution process have been integrated into the probabilistic framework in a manner consistent with NUREG/CR-5423.

In my original review of NUREG/CR-5423, which is included in Appendix G of that report I stated:

"Any probabilistic framework estimating the confidence level regarding a conclusion on a phenomenological question must begin with a decomposition into the major components of the physical process. On page 5 the authors state, "The above decomposition is not unique, nor does it consider explicitly all uncertainties." I agree with this statement and for this review the essential question is whether this decomposition is sufficient to support the decision making process."

With respect to the continuing efforts to resolve the four specific technical issues I believe that, while the specific uncertainties associated with the individual issues are likely different in the minds of different technical experts, the process defined and carried through by the NRC to resolve these issues is sufficient to support the decision making process. I commend you and your staff, as well as the authors of the report for following through with the review, comment and resolution process.

The four areas of concern were:

1. the effect of additional steel from the melting of upper internals on the composition of the core debris discharged into the drywell,
2. the duration of superheat in the melt as core debris spreads across the drywell floor,
3. the spreading of the core debris and the depth of the material next to the liner, and
4. the criteria for creep rupture failure as the liner temperature increases.

These have been addressed through separate expert group meetings and the development of additional calculations by organizations which were not involved in the original study. The fact that these were performed by other organizations adds to the credibility of the technical resolution in addition to the review and comment process already carried out by the NRC. I have reviewed the material related to the APRIL calculations, those associated with the MELTSPREAD-1 calculations for the corium depth next to the Mark I liner, the CORCON calculations and the 3D structural calculation for the Mark I liner. My assessment for each of these contributions follows:

APRIL Calculations

The APRIL calculations appear to have been performed in a manner which maximizes the steel contribution to the molten material that could be discharged from the reactor vessel into the containment. In this regard, they provide an upper bound of the mass of metallic materials that could be discharged at the time of reactor pressure vessel (RPV) failure. Given the assumptions, the results appear reasonable and provide additional justification that melt releases used in NUREG/CR-5423 bound those developed from a mechanistic calculation.

MELTSPREAD-1 Calculations

In general, these calculations appear to be well conceived and documented. The results support the original causal relationship modeling in NUREG/CR-5423 and thereby address the questions which arose during the review process. I have two minor comments with respect to the modeling approach for the MELTSPREAD-1 calculations which produce inherent conservatism in the approach.

1. The general behavior modeled is melt spread over a concrete floor covered by water. As the melt spreads over the porous concrete, water would be trapped in the pores. Rapid heat transfer and vaporization of this water, perhaps localized explosive interactions, would slow the melt progression and cause enhanced energy transfer with the overlying water. Such interactions would slow the melt spreading to the

drywell wall. This is not represented in the current calculations.

2. Available experimental results for molten materials "spreading" through water would suggest that the net energy transfer rate to water would approach 30 MW/m² while the debris is fully molten. This is due to dynamic melt-water interactions, some of which could result from Item 1 above. Whatever the detailed mechanisms, such enhanced energy transfer would slow the melt progression to the drywell wall and the thermal transient of the wall once contact occurs.

CORCON Calculations

I have reviewed the CORCON-MOD3 results. These provide the necessary audit calculations for the approach used in NUREG/CR-5423. Therefore, these resolve the issue of the duration of significant superheat.

Creep Rupture Calculations

The 3D finite element calculations provide the necessary supporting analysis for failure conditions at temperatures less than steel melting. As I mentioned in my earlier review of the creep rupture calculations, there are conservatisms in the assessment of the transient wall temperature profiles which are likely of significance.

1. As the wall is calculated to deform, the debris crust is assumed to move with the wall instead of opening a gap which would slow the energy transfer rate.
2. Perhaps more importantly, as the debris spreads through the water and contacts the wall in the region which was submerged, one should expect a significant contact resistance due to the vaporization of water in the steel surface cavities or due to burning or melting of paint on the liner. This would slow the entire thermal transient and reduce the peak wall temperatures for the same initial conditions.

NUREG/CR-6025

I have reviewed the main document. The authors have properly characterized the additional studies and have incorporated the results into their probabilistic framework in a consistent manner. Some other comments are pertinent to parts of the document and the ROAM process.

1. I do not agree with the statement on page 1-14 that "melt running directly into the downcomer - such an occurrence would almost certainly produce failure." The condition of melt entering a downcomer in the presence of water was not discussed in the review and comment process. For example, how deep would such a melt steam be for the flow rates in the different scenarios? Such speculations run contrary to the disciplined approach created by ROAM. Hence, if this is an

accident situation to be addressed, then the ROAAM methodology could be applied to the specific configuration. I recommend that the current statement be deleted.

2. Similarly, the discussion on steam explosions in Appendix A was not part of the disciplined review, comment, analysis and resolution process. Moreover, it is not discussed in the same level of depth as the other issues in the original and final reports. I suggest this be deleted and if it is of sufficient interest, apply the ROAAM methodology separately.

Also, Figure 8 on page 1-12 appears to have some symbols switched.

Therefore, in answer to your two questions, I believe the four individual issues have been resolved and the results have been integrated into the probabilistic framework in a manner consistent with NUREG/CR-5423.

Thank you for the opportunity to review this important work. Once again let me compliment the authors of the report and the NRC staff for pursuing the resolution of this issue through a disciplined process. Please call should you have any comments on my review.

Sincerely yours,

Robert E. Henry, Senior Vice President
Process Safety Department

REH:jal

cc: Dr. Sud Basu, NRC

May 24, 1993

Dr. Brian W. Sheron, Director
Division of Systems Research
Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
Washington, District of Columbia 20555

Dear Dr. Sheron:

This is in response to your letter of April 27 in which you requested that I review the draft NUREG/CR-6025 entitled *The Probability of Mark I Containment Failure by Melt-Attack of the Liner* and provide my opinions as to

- (a) whether the four individual issues (described in Parts 2 through 5) have been sufficiently resolved given the state of our current knowledge on these issues; and
- (b) whether the results of the resolution process mentioned under the previous item have been integrated into the probabilistic framework in a manner that is consistent with NUREG/CR-5423.

The comments elicited by my review are provided in the Attachment. Please let me know if you desire additional clarification concerning the bases for these comments. Thank you for the opportunity to participate in this important review.

Sincerely,

Stephen A. Hodge
BWRTT Program Manager

SAH:rcp

Attachment

cc/att: S. Basu, NRC
Y. Chen, NRC
F. Eltawila, NRC
C. E. Pugh
File - SAH - NoRC

*The Probability of Mark I Containment Failure
by Melt-Attack of the Liner*

Review by
S. A. Hodge
Oak Ridge National Laboratory

This report and its companion document NUREG/CR-5423 provide, for the first time, an integrated evaluation of all aspects of the question of potential failure of the BWR Mark I drywell shell as a result of melt-attack under severe accident conditions that involve the release of debris from the reactor vessel. As such, these constitute valuable contributions to the literature, providing a valid framework within which individual pieces of this complex puzzle have been assessed separately, with the results then utilized at their proper places within the overall solution process. The overall dénouement of such a complicated question that has been accomplished by this process is truly impressive.

Let me also state at this point that I am personally convinced that the results yielded by this research process are basically correct. To wit, I am convinced that any BWR Mark I facility that has been upgraded so that the reactor vessel can remain depressurized and the drywell floor can be flooded under severe accident conditions including station blackout is indeed protected against failure of the drywell shell by melt-attack during the initial period of debris release from the vessel.

Nevertheless, several questions did enter my mind during my review of the current document drafts. I believe there is a reasonable probability that these same questions will arise for future readers if modifications are not made and accordingly, I describe these questions in the following sections of this review. I have also offered suggestions toward revisions that might improve the clarity of the various parts of NUREG/CR-6025.

PART I
THE PROBABILISTIC SYNTHESIS

Part I of the draft NUREG/CR-6025 in effect provides a summary of the effects of issues described and conclusions reached in Parts II through V. I found it expedient to briefly scan Part I, then to study Parts II through V, then to return for a careful consideration of the points made in Part I. I recommend that a parallel procedure be followed by readers of these comments.

1. Figure 2, which accurately depicts the situation for the Peach Bottom drywell, differs from Figure 6 of Part V, which is correct for the Browns Ferry drywell. As indicated in Figure 2, the opening for one of the vent pipes to the pressure suppression pool does lie directly opposite to the pedestal doorway at Peach Bottom.
2. What is meant by "Addendum 1" in the last paragraph on page 1-8 and the first paragraph on page 1-11?

3. I recommend changing "that liner failure...is physically unreasonable" to "that early liner failure...is physically unreasonable" in the last sentence of Section 2.2 on page 1-10.
4. With regard to the last sentence of Section 2.3 on page 1-11, I expect that the typical reader of Part III will have the impression that the authors of Part III have emphasized that CORCON Mod3 is not accurate during the period under consideration (the period of the initial decrease of the melt temperature to the liquidus temperature).
5. With respect to the first paragraph of Section 2.4 (page 1-11), it should be pointed out that the ANATECH work was based upon a drywell configuration in which the pedestal doorway does not face a vent pipe opening, which is not true of Peach Bottom. (It is true of Browns Ferry.)

Second, I do not believe that a typical reader of Part V would consider the stated finding of 1260°C to be "conservative." Rather, I believe that Part V indicates that a failure temperature of 1200°C should be utilized, if one desires a 95% probability that the actual failure temperature would not be lower than the temperature utilized. Thus, use of 1200°C would be "conservative."

6. In the second paragraph of Section 2.4 (on page 1-12), mention is made of "the best estimate CR2 based on CORCON Mod3." My comment 4. above applies here as well. For the reasons stated, I do not believe that Part III in its current form provides any basis for modification of CR2.
7. Based upon Comment 6 above, I recommend that Tables 5 and 6 be revised as necessary to reflect an FCE-2 based upon the same CR2 as utilized in NUREG/CR-5423.
8. With respect to the first sentence in the first paragraph of Section 4 (page 1-14), it should be reported that the vent line configuration considered in Part V is correct for Browns Ferry, but not for Peach Bottom.

Also, the nature of the "recommended guidelines" cited in the last sentence of this paragraph is not clear. Do these instruct the licensee to consider whether or not the reactor vessel would remain depressurized (even for long-term station blackout), whether or not the drywell could be flooded, and whether or not drywell pressure might exceed 29.4 psia (the basis for the creep rupture calculations of Part V)? In short, an expanded discussion of the items specified by the guidance is recommended.

9. Clearly, the nature of the drywell shell immediately opposite the pedestal doorway should be included in the list of plant-specific construction items discussed in the second paragraph of Section 4 (page 1-14). Specifically, is there any reason to believe that Peach Bottom is the only U.S. BWR facility that has a vent pipe penetration immediately opposite the pedestal doorway?
10. The last sentence of the first paragraph of Section 5 (page 1-15) should include the provisos:...if the reactor vessel is depressurized at the time of failure, if the containment pressure is not greater than 29.4 psia, and if there is no vent pipe immediately opposite the pedestal doorway.
11. With respect to the ES-PCM steam explosion scenario discussed in Appendix A of Part I, wouldn't such an event increase the containment pressure beyond the 29.4 psia considered in the Part V analysis of the creep rupture failure temperature? This argument strengthens the recommendation for the provisos listed under comment 10. above.

12. Typographical errors noted in passing:

- a. "PRI" should be "RPI" in paragraph 1 on page 1-5.
- b. For clarity, "Appendix A" should be "Appendix A of Part I" in the last paragraph of Section 1 on page 1-6.
- c. "average relase" should be "average release" in the first paragraph of Section 2.2 on page 1-8.
- d. "melth" should be "melt" in Section 2.3 on page 1-10.
- e. "(or any containment)" should be "(for any containment)" in the last paragraph of Section 4. on page 1-15.

PART II

THE EFFECT OF HEATUP & MELTDOWN OF REACTOR VESSEL UPPER INTERNALS ON THE COMPOSITION OF CORIUM DISCHARGED INTO THE DRYWELL OF A BWR MARK-I CONTAINMENT

Since I served as the Program Manager for this project and had ample opportunity to comment during the drafting stages for the Part II document, I will limit my remarks here to a clear statement of the objective as I understand it. Scenario I as considered in NUREG/CR-5423 derives from a MAAP code analysis whereas Scenario II derives from a BWR SAR code analysis. Scenario I represents an initial release dominated by oxides whereas Scenario II represents an initial release dominated by metals; neither involves any significant quantity of superheated material.

It was postulated that the actual case might lie somewhere in between by virtue of melting of the overlying stainless steel structures within the reactor vessel. Neither MAAP nor BWR SAR has good models for this, but APRIL.MOD3 does. Therefore, it was decided to sponsor an APRIL analysis to determine whether or not a significant quantity of metals (from melting of the steam separator/dryer complex) might be mixed with oxides released from the vessel in a best-estimate calculation.

PART III

AUDIT CALCULATIONS WITH CORCON-MOD3 OF THE DURATION OF SUPERHEAT IN NUREG/CR-5423

Part III provides an interesting discussion of the meaning of "superheat" with respect to a melt of core and structural debris overlying concrete. The superheat duration considered in NUREG/CR-5423 is clearly defined as "the time required for the melt to fall to the liquidus the first time." It is further explained that subsequently the liquidus temperature may decrease (because of the introduction of concrete decomposition products) faster than does the melt temperature, so that "superheat" would be reestablished.

My general impression after reading Part III is that while the results of this work certainly do not pose any challenge to the duration of superheating utilized in the NUREG/CR-5423 analysis, they also do not provide any basis for reducing the period of superheat duration used in that study. The latter conclusion derives from the discussions in Part III relating to the applicability of CORCON-MOD3 to this initial period of melt cooling:

"Especially for shallow melts, the time required for the melt temperature to drop to the liquidus is so small that results of CORCON calculations cannot be considered very accurate." (Section 4.1, page 13.)

"It was long ago decided that the contact transient when melt first begins to interact with concrete need not be modeled in CORCON." (Section 4.1, page 13.)

"A usual time step for CORCON calculations is about 1 minute. A rule of thumb is that the first 4 or 5 time steps may not be very accurate.... it is during this initial transient that bulk debris temperatures are predicted to fall to the mixture liquidus temperature." (Section 4.1, page 13.)

"It cannot be claimed that CORCON is an especially accurate model for the short initial periods of interactions of melt with concrete." (Section 4.2, page 14.)

Based upon these observations, I have the following recommendations with respect to Part III:

1. The authors should provide a clear statement as to whether or not they believe the results of their work justify any reduction in the period of superheat duration as utilized in NUREG/CR-5423.
2. The first paragraph of Section 3 (page 6) should be revised to also point out that Scenario I is the case most threatening to the drywell shell. As currently written, this paragraph suggests that Scenario II was neglected for trivial reasons; in fact, Scenario II provides no threat for early containment failure and hence there was no need to consider it here.

PART IV

COMPARISON OF MELTSPREAD-1 CALCULATIONS OF CORIUM DEPTH NEXT TO THE MARK-I LINER WITH NUREG/CR-5423

I found the Part IV analysis convincing and easy to comprehend and I have only minor suggestions for clarification.

1. Why is a containment pressure of 0.300 MPa (43.5 psia) used for the Part IV analysis (Table 3.8), whereas the pressure used for the creep rupture analysis in Part V is limited to 0.2 MPa (29.4 psia)?
2. The penultimate paragraph on page 3-2 provides "...all other U.S. Mark I units have similar pedestal wall and liner diameters while the total sump volumes...are greater than or equal to the Peach Bottom sump volume..." I don't know to what extent the validity of this statement has been checked, but there are seven different sizes of reactor vessels in U.S. Mark I containments, ranging from Duane Arnold (183 in. ID) to Browns Ferry (251 in. ID). At Duane Arnold, the radius to the liner at the drywell floor is 6.34 m (20.8 ft),

whereas at Browns Ferry it is 6.96 m (22.85 ft). Such comparisons of containment dimensions seem to be meaningless, however, unless the size of the core is also taken into consideration (Browns Ferry has 764 fuel assemblies as compared to the 368 at Duane Arnold).

In summary, I recommend that consideration be given to removal of this sentence from the Part IV report. The NUREG/CR-5423 analysis clearly pertains only to Peach Bottom, and it is not necessary to speculate here concerning the dimensions of other plants.

3. Typographical errors noted in passing:
 - a. "Calculations" is misspelled in the title of Table 4.3.

PART V

CREEP-RUPTURE FAILURE IN A MARK-I DRYWELL LINER

I consider Part V to be the most important of the independent evaluations from the standpoint that only the Part V results required a recalculation of the final conservative estimates (FCEs) of liner failure probability as reported in Part I. Obviously, any additional modifications to these results would be expected to again affect the FCEs.

1. 1.0 Introduction (Page 3). The statement that the assumption that the creep properties of the steel above 1100°C are the same as those measured at 1100°C "probably is a non-conservative approximation" is somewhat unsettling. It would be desirable for the authors to provide some estimates as to the potential impact of this.
2. Section 2.1 Finite Element Model – Subsection 2.1.1 Grid (Page 6). "The penetrations of the liner by downcomers...are not modeled in the three-dimensional analysis." One of the Peach Bottom downcomers is located directly opposite the pedestal doorway (Figure 6 is not correct for Peach Bottom). It seems that the presence of this downcomer must affect the calculated creep rupture failure temperature and the report should provide at least a qualitative discussion of what this effect would be.
3. Section 2.2 Applied Pressure and Temperature Conditions (Page 6). Here it is explained that the analyses is based upon a constant 0.2 MPa (29.4 psia) pressure. In my opinion, the pressure may very well be higher than this and I would like to have the author's estimate as to the effect upon the creep rupture failure temperature results if a pressure of say 43.5 psia (0.300 MPa) as was used as the basis for the Part IV Meltsread calculations had been employed (instead of 29 psia) for the creep rupture analysis.
4. Section 3.0 Failure Temperature Distributions. Figures 20 and 21 and Table 2 seem to indicate that if an assumed failure temperature was desired such that there was a 95% probability that the actual failure temperature was not lower than the assumed value, then this assumed failure temperature would be about 1200°C for element 7.

To state the converse, it seems that the creep rupture failure temperature of 1260°C cited in the Abstract and in paragraph two of the Conclusions (page 40) is such that there is a 95% probability that the actual failure temperature is not higher than this. If this is true, then a value of 1200°C, not 1260°C should be used in the Part I analyses.

I recommend that the basis for the creep rupture failure temperature of 1260°C as stated in the **Abstract** and the **Conclusions** of Part V be clearly explained in relation to the failure temperature distributions provided in Section 3.0.

5. Typographical errors noted in passing:

- a. "sound" should be "second" in the 5th line of the right-hand column on page 4.
- b. "uncertainty, distributions" should be "uncertainty distributions" in the middle paragraph on the right-hand column on page 31.
- c. "form" should be "from" in the first paragraph of the left-hand column on page 33.

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June 23, 1993

Dr. Brian W. Sheron
Director
Division of Systems Research
Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
Washington, DC 20555-0001

Dear Dr. Sheron:

I have reviewed the draft report "The Probability of Mark-I Containment Failure by Melt-Attack of the Liner," (NUREG/CR-6025) and have the following comments on the contents.

As a whole, I think the report has provided an in-depth analysis of the four issues raised about the methods used in NUREG/CR-5423. Three of these issues proved to be treated conservatively in the original report. One, namely the liner failure criterion, was found to be too optimistic but even with the more conservative newly adopted criterion, the findings of the original report remain qualitatively appropriate. I will address first the resolution of each of the four issues described in Parts 2 - 5, then provide a comment on the adequacy of the total effort.

(1) MELT RELEASE CONDITIONS

The investigation by the RPI group described in Part 2 has applied the computer code APRIL.MOD3 to assessment of the impact of a variety of assumptions on the melt conditions. In particular, the impact of uncertainties in radiation heat transfer, core cooling by the CRD flow, axial power distribution and early vessel failure. In all cases the amount and rate of corium melt relocation to the containment flow fell within the range of conditions assumed in NUREG/CR-5423 study. The report by the RPI group correctly states that "the bounding conditions in NUREG/CR-5423 seem to be adequate." The details of the APRIL-MOD 3 code assumptions can be argued about in the manner that Denny of SAIC does. While he comments correctly that more oxidic material may be

Dr. Brian W. Sheron
June 23, 1993
page 2

present in the corium than the code would predict, there are good reasons to believe that the melt would remain mostly metallic. One reason, simply stated, is that the oxide entrainment and steam generation (which may lead to Zr oxidation) are controlled by the basic amount of steel melting and relocation to the vessel bottom. Hence, that process would dominate and be the driver for the melting conditions. My conclusion is that the APRIL MOD 3 calculations would remain essentially adequate as bounding for the expected corium conditions. I am of the opinion that this set of calculations adds considerable confidence to the base of calculations by MAPP and BWRSAT codes which were the bases of the NUREG/CR-5423 calculations.

The integration of the APRIL modeling effort in NUREG/CR-6025 includes discussion of the range of predicted conditions and the reservations by the working group members Schmidt and Denny (Addendum 1). I am in agreement with the jest of the discussion which negates the possibility of a much larger metallic melt in a Scenario I type accident. The formation of a strong crust to carry the molten fuel in core precludes the development of vigorous steam convection to the upper steel structures. Furthermore, the shorter time to vessel failure in Scenario I limits the time available for upper steel heating. The resultant melt in a Scenario I is likely to be largely oxidic, as assumed originally in the NUREG/CR-5423 report.

(2) THE SUPERHEAT DURATION

The investigation in Part III (erroneously referred to in the first part of the report as Part IV) appears to comprehensively cover the possible impact of initial depth, temperature and Zr fraction on the superheat possible in an oxidic melt (Scenario I). The results of the independent investigation using CORCON-MOD3 indicate that the values used for superheat duration in

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NUREG/CR-5423 were conservative. Only one case out of seventeen shows CORCON predicted duration slightly larger than that used in the original report (Case 16 in Table 4). The only cautionary remark I have about this set of calculations is the well known uncertainty about the CORCON results in the initial few minutes (2-3) of a calculation. However, this seriously affects only the shallow melts which would not pose a threat to the liner anyway.

As for the integration of the CORCON results within NUREG/CR-6025, it was done as an alternative (best estimate) evaluation for liner melt probability. It is in fact informative to show the impact of the CORCON predicted superheat duration on the calculation.

(3) MELT SPREADING

The investigation by ANL of the impact of melt conditions on the spreading behavior of the melt in the drywell relied on the code Meltsread-1. The variation of the parameters used covers the range of interest to Scenarios I and II, and the results were found to lie within the bounds of NUREG/CR-5423 for Scenario I and to be significantly less threatening than these bounds for Scenario II. Even though the assumptions of the Meltsread code may be too simplistic as far as the upward heat transfer is concerned and the non-newtonian fluid properties of the melt, I do not believe the outcome would be significantly different with a more rigorous treatment that accounts for crust formation on top and at the bottom. Another question that can be raised: What is the effect of completely neglecting the heat transfer to water, or having the water at a higher temperature than assumed here?

As far as the integration of the results in the new report, it appears that the authors preferred not to take advantage of the effect of spreading on the Scenario II results which

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leaves the report over conservative for this more probable case.

(4) THE LINER FAILURE CRITERION

A creep rupture based failure criterion was produced by the ANATECH team. This is indeed the most appropriate failure mechanism. Based on three dimensional analysis using the ABAQUS code, creep rupture data of a ferritic steel similar to the drywell steel that shows strain to failure at the high temperature end of 10-20%, the report predicted an effective failure temperature of 1260°C. It should be mentioned that the data used ends at 1100°C. For higher temperatures the behavior was assumed similar to the 1100°C. As pointed out by the BNL reviewers, at higher temperatures larger strains may still be possible, as demonstrated by steel forging practices.

Another point to note is that while the behavior assumes a drywell pressure of 0.2 MPa, that pressure is easily transmitted to the back of the liner via the existing large openings above the melt location. Hence, the deformation found from this analysis may be exaggerated when pressure equilibration on both sides of the liner is taken into consideration. However, the deformation rate at higher temperatures than 1100°C will likely exceed that at 1100°C. Given that no sensitivity to the drywell pressure was given, it is not possible to tell the net effect for sure. However, given the potential for higher failure strain criteria than allowed here, the present analysis is clearly a conservative one.

The integration of the results in Part I is done at two temperatures: 1260°C, the recommended failure criteria, and 1200°C, as a bounding criterion. This extra measure of conservatism is prudent given the differences in the uncertainty in scenarios of melt attack on the liner. Furthermore, while the ANATECH results are for a 20 cm deep layer, the synthesis applied the same failure

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temperature criterion for the shallower depths of 15, 10 and 5 cms. This will add to the conservatism. On a minor point, I think the curves shown in Figure 8 have a reversed order of melt height.

Applicability to Other Mark-I Reactors

The main variables affecting the outcome are those pertaining to the amount of melt, its initial superheat, and drywell geometry. The report argues that variations in these parameters are small and in the few cases in which they are appreciable they do not have a significant consequence. I did not seek to verify these statements and would have liked to see a table to show variations in the most important parameters for all Mark I containments.

Concluding Remarks

The findings of this report appear sufficiently backed by the evidence presented. Given that the Meltsread calculations used a single water temperature, a question arises about the impact the water temperature in the drywell may have on the outcome of the Meltsread calculations. If either the expected range of possible temperatures or the sensitivity to temperature is small, then the current situation is acceptable. In NUREG/CR-5423 this temperature effect was indirectly addressed by applying various limitations to the upward heat flux from the melt to the water. This is sufficient to indicate that the report is based on the best available knowledge today with adequate conservatism. In fact for Scenario II, the more probable scenario, the report may be too conservative.

Sincerely,

Mujid S. Kazimi

MSK:ewp

John T. Conway, Chairman
A.J. Eggenberger, Vice Chairman
John W. Crawford, Jr.
Joseph J. DiNunno
Herbert John Cecil Kouts

DEFENSE NUCLEAR FACILITIES SAFETY BOARD

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May 12, 1993

Dr. Sud Basu
Division of Systems Research
Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
Washington, DC 20555-0001

Dear Dr. Basu:

As I informed you by telephone today, I shall be unable to review the draft report entitled "The Probability of Mark-1 Containment Failure by Melt-Attack of the Liner." I am no longer at Brookhaven National Laboratory, and in my present position as a member of the Defense Nuclear Facilities Safety Board I do not have the spare time that would be required for so formidable a review.

I regret the inability to help you in this matter, because for some time I have followed the topic of the review you have requested and am pleased to see it heading for resolution.

Sincerely,

Herbert Kouts

Cc. Brian W. Sheron

UNIVERSITY OF MARYLAND AT COLLEGE PARK

DEPARTMENT OF MATERIALS AND NUCLEAR ENGINEERING
NUCLEAR ENGINEERING

June 1, 1993

Dr. Brian W. Sheron, Director
Division of Systems Research
Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
Washington, DC 20555

Re: Review of NUREG/CR-6025

Dear Brian:

Thank you for asking me to comment on NUREG/CR-6025, "The Probability of Mark-I Containment Failure by Melt-Attack of the Liner" by T.G. Theofanous, et al. Generally, I find the new information provided in this report substantial, and a necessary complement to NUREG/CR-5423. I have mainly focused my review on the probabilistic treatments of this study. My specific comments are listed below:

1. In my previous reviews, I expressed a concern over possible interdependencies in the PDFs and CRs. I gave some examples of such interdependencies too. The subsequent response given in NUREG/CR-5423 was that these are unimportant interdependencies and should not be considered. Now that ROAAM is more formally presented in NUREG/CR-6025 as the basis for potential resolution of the liner failure issue, I still fail to see how one ensures that interdependencies are properly considered and treated in a ROAAM process. I think that ROAAM as a formal methodology for probabilistic treatment of physical processes and for future resolution of similar issues should address this concern. I believe that ROAAM deserves additional careful evaluation and description, so that other users can effectively use it in similar applications.

In addition, I would like to see that a formalism is described in connection with ROAAM through which experimental results, deterministic calculations, and general knowledge about an event or a parameter are translated to DPDs.

2. I am concerned about the "conservative" probability estimates and assumptions used. Here, the final results do not represent a "best estimate". In a probabilistic framework only a best estimate and probabilistic bounds around that estimate should be calculated. In many places in Part-I of NUREG/CR-6025, the authors use "conservative estimate"

as justification for not using more realistic results. For example, in section 2.1 for the Scenario II with a large oxide fraction, conservative initial conditions of NUREG/CR-5423 are used.

Also in section 2.3 the authors have used the original "highly conservative" value for CR2. The justification is that based on "The uncertainties in the rather complex, multicomponent chemistry and phase-behavior involved, this (conservative value) may not be too inappropriate". I believe that the authors should either agree with the results of CORCON.Mod3 (as both R.E. Henry and B.W. Spencer have agreed), or they should state what is the exact nature of their disagreement. If the authors agree with the results, then the best estimate results of CORCON.Mod3 should be used. Thus, FCE-1 becomes meaningless. Otherwise, calculation of CR2 remains to be a diverging issue.

In the synthesis of the results, only two "enveloping" calculations for scenario-I are presented. I fail to see how these two calculations sufficiently envelope the spectrum of the results. For example, it seems to me that use of 1200 C in FCE-1 gives a more conservative estimate of the probability. Also the conclusion of "physically unrealistic" for the occurrence of FCE-1 and FCE-2 appears to contradict the scale provided in Table 2 of NUREG/CR-6025. I interpret Table 2 for the processes having a likelihood between 1/100 and 1/1000 as those that "cannot be positively excluded". Incidentally, this is not unreasonable, since individual likelihood of a fatality due to an automobile accident in the U.S. over a 10-year period, or individual annual likelihood of developing any type of cancer is about 2×10^{-3} . In my view, these occurrences are not "physically unreasonable", nor they "violate well-known reality".

3. The general discussions provided to extrapolate the conclusions beyond the Peach Bottom results (Section 4. of Part-I) is premature and simplistic. There are many assumptions that are either specific to Peach Bottom or they may be valid for specific BWRs. For example, Part-I results are specifically directed towards the analysis of severe accident scenarios with timely reactor depressurization event. To the extent that this or other similar assumptions applies to other BWRs, one needs to perform more careful evaluation before general conclusions can be derived. I suggest that the authors only mention the need for a systematic look at the controlling features of other Mark-I containments, and the need to estimate the effect of these feature on the corresponding liner failure probabilities.

In conclusion, I view the results and methods provided in NUREG/CR-6025 and NUREG/CR-5423 as major steps forward for a final resolution of Mark-I liner failure issue. I think that the ROAAM process is a superior method for integrating a wide spectrum of physical information into a final probabilistic estimate than the direct solicitation of expert opinion method used in NUREG-1150 (ROAAM uses some form of judgment to come up with its PDFs and CRs, but this is done at a level where more information is available). I believe, however, that ROAAM deserves more attention and scrutiny before it can be adopted in applications beyond the liner issue.

Thank you for giving me the opportunity to comment on this report. Please don't hesitate to contact me if you have further questions.

Sincerely Yours,

A handwritten signature in black ink, appearing to read 'Mohammad Modarres', written in a cursive style.

Mohammad Modarres
Professor of Nuclear Engineering

cc: Dr. Sud Basu, NRC
Dr. Mohsen Khatib-Rahbar, ERI

Dr. Sud Basu
Accident Evaluation Branch
Division of Systems Research
Office of Nuclear Regulatory Research
Washington, DC 20555

June 25, 1993

Dear Sud,

I have reviewed the draft report, "The Probability of Mark-I Containment Failure by Melt Attack of the Liner," (NUREG/CR-6025).

It is my opinion that the four areas addressed by working groups,

1. Melt Release initial Conditions
2. Melt Spreading to the Liner
3. Corium Concrete Interaction
4. Liner Failure Criteria

have been appropriately integrated into the probabilistic framework of NUREG-5423. The resulting conclusions of NUREG/CR-6025 remain in agreement with those of 5423.

The concerns raised at the Harper's Ferry meeting gave opportunity for technical experts to challenge the bases of the ROAAM approach and inputs. The working group process allowed expertise to be focused on quantities where confidence may have been lacking. The follow-up work appears to have succeeded in establishing a higher level of confidence in the ROAAM input probability ranges and causal relationships employed.

One outstanding characteristic of the resolution process applied to the Mark I liner issue is that it was possible for differing opinions, skepticism, and doubts to be freely expressed, resolved, and incorporated in the final conclusions. The result is a unified stance on a lingering technical issue.

The melt release conditions were further analyzed by the APRIL program, and results were found to be conservatively represented by the NUREG/CR-5423 conditions employed.

The melt spreading to the liner causal relationships CRI were found to be either appropriate or conservative for both scenarios I and II, based on detailed computations of the ANL code, MELTSPREAD.

The corium-concrete interaction, strongly dependent on melt superheat, was found to be conservatively addressed in the ROAAM application, based on CORCON-MOD 3 superheating predictions.

Finally, the liner failure criteria work of ANATECH resulted in lowering the NUREG/CR-5423 temperature from 1500 C to 1260 C for a conservative interpretation. The topic of creep rupture is not my expertise, although the arguments seem reasonable, leading to conclusions which still lie within the bounds that in the presence of water, liner failure is physically unreasonable.

The section on extrapolation to other Mark I plants calls attention to two cases where the downcomer height above the drywell floor is zero, which could permit molten core debris to flow into the downcomers. Since the associated amounts of corium released differs from plant to plant, IPE's which employ the ROAM procedure should help determine if melt could reach the downcomers, or if plant modifications would be necessary.

The appendix of NUREG/CR-6025 presents a discussion of the potential for steam explosions causing failure of the Mark-I pedestal. It is shown that even in the worst instance, venting of the rapid steam formation would prohibit destructive pressure loading on the pedestal.

My summary opinion is that the four issues addressed in the NUREG/CR-6025 draft have been resolved, and the working group findings have been appropriately incorporated into the ROAM of NUREG/CR-5423. Moreover, the steam explosion consideration has provided a basis for concluding that the pedestal will not experience destructive loadings from molten debris pouring into contained water.

Yours truly,

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June 17, 1993

Brian W. Sheron, Director
Division of Systems Research, Mail Stop NLS007
Office of Nuclear Regulatory Research
US Nuclear Regulatory Commission
Washington, DC 20555

Re: Comments on "The Probability of Mark-1 Containment Failure by Melt-Attack on the Liner."

Dear Mr. Sheron:

I have examined at your request the Probabilistic Synthesis and supporting documents that make up NUREG/CR-6025. "The Probability of Mark-1 Containment Failure by Melt-Attack on the Liner." I believe there are two areas that need attention in defending this body of work. One area is the definition of the bounding scenarios. The other area is the definition of a failure criterion for the liner. I comment on these items below in an effort to define a strategy to defend the body of work in NUREG/CR-6052 before a skeptical audience.

1. Definition of the Scenarios.

Concerns expressed by Denny and Schmidt in their reviews of Part II of NUREG/CR-6052 are highlighted in Part I, but they are not cleanly resolved. Denny is concerned that the amount of molten oxide present in the initial melt may be larger than is postulated for Scenario II. He cites several reasons for this concern. These reasons all seem to involve phenomena not modeled in the computer codes BWRSAR, APRIL or MAAP.

One of Denny's concerns is the chemical dissolution of oxide by alloys of steel and zirconium. This, I believe is not an important effect. Whereas zirconium exhibits a pronounced solubility for oxide, steel does not. The solubility of oxide in stainless steel is negligible for the analyses of core degradation. Alloying of zirconium with stainless steel involves transfer of electrons that reduces the solubility of oxide in the alloy. I believe this can be demonstrated quantitatively by an analysis of the Zr-Fe-O or Zr-Ni-O ternary systems. Data needed for these analyses are available.

Nevertheless, Denny's point remains for other reasons. Denny concludes benignly that the existing analyses might be adequate. This suggests that Denny's concerns might be properly addressed through the probability density functions used in Part I. It might be possible to show that upper bounds on the appropriate probability density functions have been selected to capture at least qualitatively the effects of phenomenological uncertainties Denny cites. The issues, then, would not have to be left open as they are now in Part I of NUREG/CR-6052. Leaving these issues open produces a sense that more work must be done. Discussion of recent CORA, Ex-reactor, and DF tests might be useful in this context.

Schmidt expresses the concern that there may be more metal present in the initial melt for Scenario I than is now considered. The melt composition for Scenario I was based originally on results of a MAAP calculation that did not include an extensive treatment of heatup and melting of the upper core internals. The concern is that the contribution of molten steel from these upper internals to the Scenario I melt may not be properly estimated. The APRIL calculations presented in Part II show melting of the upper internals occurs. But, Podowski and Cho indicate that the configuration hypothesized in the MAAP calculations is not calculated to develop by the APRIL code. They make this statement with a proviso. That is, the crucibilized configuration does not develop if the coolant system has been depressurized by operation of the ADS.

This restriction of the conclusions to depressurized circumstances is very problematic. NUREG-1150 certainly indicates that accidents involving a failure to depressurize make significant contributions to the core melt frequency at one Mark-I BWR. It might be argued that planned improvements to the ADS make the NUREG-1150 results anachronistic. This, however, will have to be explicit in the document.

Only in case 8 are APRIL calculations done for core degradation in a pressurized system. These calculations yield results that are, it seems, within the band assumed for the analyses in Part I. Omission of these results from Table 3 in Part I seems, then, unnecessary and likely to invite criticism.

Even the calculations for case 8 did not seem to produce the configuration hypothesized in the original MAAP calculations. For this reason, Schmidt concludes the issue of metals in the Scenario I melt has not been resolved since the heatup of upper internals for the Scenario I configuration has not been done.

There is, then, an issue of whether Scenario I actually can develop. In Part I, Theofanous and Yan seem to suggest that the Scenario I configuration with upper internals melting involves some physical inconsistencies. They cite steaming and convective heating. Yet, radiation heating is the focus of attention in Part II. It is not obvious that difficulties with radiation heat transfer described in Part II disappear for melts in the configuration hypothesized in the MAAP code. Some more extensive and, preferably, quantitative discussion of this topic ought to appear in Part I. As it is, the issue seems unresolved. It is my understanding that Schmidt believes it can be shown that metal contributions to the Scenario I melt are small, though I do not know why.

2. Definition of Failure Criterion

The original failure criterion for the liner attack analysis was melting. In Part V of NUREG/CR-6052, Castro et al. show that creep rupture can lead to failure at a lower temperature. Analyses by Castro et al. were done for melt spread over an arc of 22.4°. The calculated temperature of rupture is between about 1220 and 1260°C. There is, however, no guarantee that melt will spread over this exact sector of the liner. Cases described in Part IV seem to show angles for Scenario I of 0 to 360° with typical values near 150°. (See Table 4.1 in Part IV) The physical phenomena of creep rupture suggests that the temperature of failure should decrease as the angle subtended by debris in contact with the liner increases. Indeed, scoping calculations by Castro et al. show that the creep rupture failure temperature falls to 982 to 1100°C in the bounding case of spreading over a 360° arc. Unfortunately, intermediate cases are not available to provide some indication of the curvature in the relationship between angle of contact of debris with the liner and the creep rupture failure temperature. Certainly, a linear relation would not be expected.

An essential point made in part V is that, in contrast to the melting failure criterion, there is some inherent uncertainty in the temperature criterion of failure by creep rupture. Castro et al. address the uncertainty that arises from their damage index data base. There seems to be an additional uncertainty due to the arc of debris contact. It is not clear that these uncertainties are reflected in the results of the calculations done in Part I.

I suspect many of the reviewers will be troubled by the radical change in bases for calculations FCE-1 and FCE-2 described in Part I without text, at least, to describe the effects of changing one factor at a time. It is simply not clear that 1200°C constitutes a very conservative bound on the failure temperature criterion. Addition of text to describe the effects of just one change at a time, along with the existing rationale for FCE-2 might relieve the concerns of some reviewers.

Finally, I comment on the niceties of presentation of the results. Table 6 in Part I provides point estimates of the results. At the meeting in Harper's Ferry concerning the original analyses, several reviewers asked that the entire probability distribution be provided. All of the necessary information to generate the entire distribution exists but it is not in the documentation. I suspect reviewers are going to ask again for the entire probability distributions. The title of Table 6, "Final Conservative Estimates . . ." just invites such a request. The qualitative term conservative in a very quantitative analysis is hard to interpret. There should be no reluctance to provide the complete distributions. The complete distributions will show just how conservative are the results in Table 6.

NUREG/CR-6052 constitutes a major accomplishment of the NRC's severe accident research program. This study augmented by source term analyses does make it clear that water in the drywell is an important accident management and risk reduction step that can be achieved at minimal cost. This coupled with the low core meltdown frequencies of Mark I boiling water reactors makes an impressive statement about the safety of nuclear power that the NRC and the nuclear industry have been able to achieve.

Sincerely yours,

Dana A. Powers, Manager
Integration of NRC Work
Program Support Dept., 6404

DAP:6404:smu

cc: S. Basu (NRC)
N. R. Ortiz (SNL-6400)
W. A. von Riesemann (SNL-6403)
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ROYAL
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Division of Nuclear Power Safety

May 25, 1993

Dr. Brian W. Sheron, Director
Division of System Research
Office of Nuclear Regulatory Research
U.S. Nuclear Regular Commission
WASHINGTON D.C. 20555-001
U.S.A.

Dear Brian,

Subject: Review of NuREG/CR-6025, Draft Report

Thanks for inviting me to review the subject report entitled "The Probability of Mark-1 Containment Failure by Melt Attack of the Liner". I have reviewed the five different parts of this report and have extensively consulted large parts of NuREG/CR-5423, the earlier report on the Mark-1 containment failure issue by Prof. Theofanous and co-workers. I am providing overall comments on the NuREG/CR-6025 in this cover letter and a set of detailed comments on the individual parts of the report in the attachment.

Overall comments

I am glad to note that much has been accomplished since we, in the community, started discussing the probability of Mark-1 containment failure during the expert evaluation and solicitation process. The answers provided at that time were intuitive and based on the knowledge of the phenomena at that time. You may recall that four out of six experts at that time provided very low probabilities for the melt-attack liner failure in the presence of water. In the ensuing five years Prof. Theofanous has developed a credible semi-probabilistic framework (ROAAM) and applied it to the resolution of this issue. In addition, there have been independent advances of technology, e.g., development of the MELT SPREAD code, clarification of the in-vessel BWR melt progression details along with the supporting experiments, advances in the CORCON code and the data on the corium-concrete phase diagrams, the data on the structural strength of pressure-vessel steels at high temperatures etc. The ROAAM methodology and the technology advances

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have helped to provide rigor (versus hand-waving) to the arguments made towards the resolution of this issue.

I consider the objectives of the work in NuREG/CR-6025 as application of the recent technological advances to confirm, or refine, the choices made for the probability distribution functions and the causal relations in the NuREG/CR-5423, and to confirm, or refine, the top-level estimates made for the probability of containment failure in the presence of water. I believe the work reported in the five parts of NuREG/CR-6025 has been successful in meeting these objectives. The refined top-level conditional probabilities for containment failure due to melt attack of $\sim 2-3 \times 10^{-3}$ are quite acceptable as physically unreasonable and, thus, should lead to the positive resolution of this issue.

A related question is whether there is some pertinent new knowledge in the offing that could change the character of the chosen probability distribution functions and the causal relations, in such a way, that the top-level estimates of containment failure probability change radically. The answer to that question is basically negative from my perspective. There will be further refinements in data, methods and codes, which will, perhaps, further refine the top-level estimate of the failure probability. However, I believe, the major refinements have been performed and there are enough conservatisms incorporated in the estimates of NuREG/CR-5423 and NuREG/CR-6025 that any non-conservatisms, in one or two of the choices made earlier, can be accommodated.

The major non-conservatism identified, and previously surmised, has been the choice of 1500° C as the liner failure temperature. The reduction in the liner failure temperature is the principal cause of the change in the top-level probability estimates. I do not believe that local heating of a small region of the three dimensional containment shell will produce failure at temperatures below 1200° C. In fact, I am sympathetic to the argument of Mr. Bandyopadhyay that data from steel forging literature should be employed to determine the failure damage index and strain rate for liner temperatures of 1200° C and above, rather than extending the measurements made at lower temperatures at INEL for pressure vessel steels.

Now, that the Mark-1 early containment failure due to melt attack on liner may be considered physically unreasonable, we must ask the question "What safety questions are important for the Mark-1 containment, in the light of the clarity achieved by focusing on the liner failure issue?". Some of the questions have been already identified by NuREG/CR-5423 e.g., the pedestal (vessel support) failure.

Perhaps the most important conclusion that emerges from the work performed earlier for NuREG/CR-5423, and that performed in support of NuREG/CR-6025, is that the melt attack on the liner occurs primarily for scenario 1 in which a massive pour of mostly oxidic material is introduced into the dry-well. In most cases of scenario 2, the melt does not reach the liner, and if it does, it will be at low temperatures. On the other hand, the earlier BWR SAR code, and the recent APRIL code, analyses strongly indicate that the BWR in-vessel melt progression does not lead to a massive oxidic material pour into the containment, but the relatively small-volume initial, and the long-duration-small-volume pours as defined in scenario 2. The mostly metallic-initial, and later oxidic, discharges of scenario 2 will freeze within, or just outside of the pedestal region and may lead to depths greater than the water depths. The stratified melt (metallic at bottom and oxidic on top) may be uncoolable. It may attack the pedestal walls and the concrete basemat, in the medium term, to (a) threaten the integrity of the pedestal walls and (b) generate enough non-condensable gases to threaten the integrity of the relatively small volume Mark-1 containments. I believe, the reactor safety questions raised by the best estimate analyses, and interpretation of the phenomena, should be the focus of our future work on Mark-1.

Steam explosions have been raised as an early containment or pedestal failure mechanism. However, I think the analysis provided in Part 1 of the NuREG/CR-6025 addresses that quantitatively and adequately. The melt coolability, the hydrogen generation and the attendant medium-term basemat and/or containment failure questions should be the focus of our attention for the Mark-1 plants. I believe that BWR scenario 2 specifics should be experimented and analysed within the context of the overall melt coolability and hydrogen generation programs of the USNRC.

The detailed comments on the parts 1 to 5 of the NuREG/CR-6025 are provided in the attachment.

With best regards,

Sincerely yours,

Bal Raj Sehgal
Professor

Attachment

ATTACHMENT

Detailed comments

I will provide detailed comments on the draft NuREG/CR-6025 in the following order: Part II, Part III, Part IV, Part V and Part I. I have also reviewed the comments of the Working Group members provided in the Appendix A of the report.

Part II

"The Effect of Heat Up and Meltdown of Reactor Vessel Upper Internals on the Composition of Corium Discharged into the Drywell of BWR Mark-1 Containment" by M. Podowski et al., Rensselaer Polytechnic Institute, Troy, N.Y.

The APRIL code appears to incorporate vessel and support plate failure models or criteria, which are quite different from those incorporated in the BWR SAR and MAAP codes and perhaps also in the MELCOR code. The results quoted show that the drain plug in the vessel fails about 19 minutes after the support plate discharges about 35 tonnes of melt into the vessel. This is unlike the predictions of both MAAP and the BWR SAR codes, since the former predicts immediate failure and the latter predicts melt quenching, subsequent reheat and, then, vessel failure. The character of the melt discharge in the APRIL code is similar to that of the BWR SAR code, on which the scenario 2 melt specification was based. One would expect that with the amount of water and structure in the BWR lower head, 35 tonnes of melt would quench. However, the modeling of the melt-water interactions and of the lower head failure are uncertain enough that the APRIL code results may well be correct.

I am somewhat puzzled by the increase in the steel melt mass obtained in case 2 on reduction of the radiation intensity by a factor of 10. The authors have explained it on the basis of the heating affected by the natural convection flow of steam when melt drops into the water of the lower head. I am not sure that the modeling of the natural circulation flow loop in the BWR vessel has been verified or validated. I have a similar question when the results for case 3 and 4 are compared.

Regarding the resolution of the issue raised about the steel content of the corium melt released to the Mark-1 drywell, I believe the APRIL code calculations described in Part

II have met the objective. The code has modeled the major heat transfer processes that may occur during the BWR in-vessel accident progression. The effects of radiation and the hot steam flow on the upper vessel steel structures have indicated that there could be a substantial steel content in the corium melt. However, the upper vessel steel will be mixed with the UO_2 which follows the initial discharge of mostly metallic corium to the drywell.

The comments of Dr. Denny do not change the main character of the corium discharge to the containment. The steel melt may well drag some additional uranium with it, however it will be a factor in the secondary slow discharge after failure of the vessel as predicted by the APRIL code.

The comment of Dr. Schmidt is concerned more with the possibility of obtaining scenario 1 type discharge in the short term station black out accident sequence. This is a very old concern, namely, whether the accident progression will be like that in TMI-2 or it will be similar to the predictions of the BWR SAR and APRIL codes. The CORA experiments, perhaps, have provided convincing answers. My current feeling is that the more probable BWR Accident sequences will result in the scenario 2 type melt discharge to the drywell, and there will be a substantial fraction of steel in the oxidic melt that is predicted to be released later in the accident sequence. One can not eliminate the consideration of scenario 1 type release, since not all accident sequences have been considered. However, the probability of it occurring should be much much lower than that for the occurrence of scenario 2 type release.

Part III

"Audit Calculations with CORCON-Mod 3 of the Duration of Superheat in NuREG/CR-5423" by D. Powers & T. Heames, Sandia National Laboratories, Albuquerque, N.M.

The calculations reported in Part III have been performed with the state of the art code CORCON-3. The objective was to verify that the causal relation CR2 of NuREG/CR-5423 gives values of the duration of superheat which are conservative, compared to the values calculated with the CORCON-3 code.

The authors have succeeded in meeting the objective and have shown that for variations of melt depth (2.3 cm to 20.6 cm), fraction of Zirconium in the metallic state (0.05 to 0.40) and the initial superheat (10K to 150K) the CORCON predicted values for the

duration of superheat are smaller than those calculated with the causal relation CR2. The authors have used the results of the CORCON code to develop a response surface, which is then used in Monte Carlo analysis to obtain probability distributions for the duration of melt superheat. Their analysis shows that there is a >90% probability of obtaining a smaller value for the duration of superheat from the CORCON-3 analysis than from CR2. Thus the issue raised about the conservatism of CR2 with respect to CORCON-3 has been resolved with the work described in Part III.

The above is nice and clean, however, I do have an adverse comment. The documentation does not provide proper information about the liquidus curve used in the calculations. Figure 1 shows the curve without any values on the ordinate of the temperature or any values on the abscissa of the mole fraction of concrete in oxide core debris. Additionally, the CORCON subroutine calculating the liquidus curve uses ideal solution chemistry and that has been questioned by M. Roach of ANL. From an early version of the draft report "Liquidus-Solidus Temperatures for Core-Concrete Mixtures" by Roache et al., the liquidus temperature measured for a mixture of 27.5 wt.% limestone-sand concrete with UO_2 - ZrO_2 , at a UO_2 - to - ZrO_2 mole ratio of 1.625:1, is quoted as $> 2365^\circ \text{C}$. I believe this value is larger than would be calculated in CORCON-3; however a comparison should be made and shown in the report. Comparisons to any other measurements of liquidus temperatures should also be made and documented. It is important to establish the accuracy of this data base in CORCON-3, since that is the reference for the calculation of the superheat and its duration.

Part IV

"Comparison of MELTSPREAD-1 Calculations of Corium Depth Next to the Mark-1 Liner with NUREG/CR-5423" by J.J. Sienecki, C.C. Chu and B.W. Spencer.

The work reported in Part IV has been performed with the state of the art computer code MELTSPREAD, which calculates the spreading behaviour of a corium melt pool deposited in the pedestal region of the Mark-1 drywell. The objective of the work was to verify that the causal relation CR1 of the NuREG/CR-5423 gives values for the melt depth of the liner, which are conservative as compared to the values predicted by the MELTSPREAD code. The need for this verification arose from the comments made by some of the peer reviewers of the NuREG/CR-5423, regarding the possibility of a lava type flow, which may result in large melt depths, in the region of the liner opposite the pedestal doorway.

The work reported in Part IV considered both scenario 1 and scenario 2 type melt pour volumes and pour rates. In each scenario the melt release volume, superheat and zirconium content were varied. In addition, large variations on heat transfer rates, chemical energy release and spreading angle were considered. Thus a sufficient number of calculations were performed to ensure a broad comparison of the results obtained on the melt depth with those obtained from CR1.

The MELTSPREAD code models, perhaps, all the physical phenomena that can influence the spreading behaviour of the melt pool. It is not a true two-dimensional spreading code, however, it approximates two-dimensionality by choosing a spreading angle for the melt on the exit side of the pedestal door. The melt spreads around the annulus after reaching the liner. The code models have been tested against some experimental data; however, with such complicated synergistic phenomena occurring and the melt physical properties changing with temperature and composition, it is not easy to rationalize all of the results obtained. The scenario 1 calculation results are relatively consistent with the variations of the parameters, however, some scenario 2 results are not easy to understand. For example in table 4.3, the cases II A 3 and II A 4 have the same initial conditions, except for a difference in melt volume of 4 m³, which leads to an increase in melt depth at the liner of 22.2 cm! Similarly, the only difference between cases II A 1 and II A 3 is 20 wt.% Zr content and that leads again to a difference in melt depth of 22.2 cm! Perhaps, some of these differences in results should be explained by the authors.

The overall spreading behaviour, shown by the MELTSPREAD code results, is quite consistent with the intuitive understanding; namely, that the melt will spread to the liner with scenario 1 type releases and not with the scenario 2 type releases (even when retention in sumps is ignored). Since, the character of the spreading during the scenario 1 type release will be primarily hydrodynamic, the CR1 of NuREG/CR-5423, which is based on Froude number scaling, should be able to describe it. Thus, we see that the CR1 5% and 95% probability melt depths are close to the results of most of the scenario 1 cases. The scenario 2 melt depth values calculated by MELTSPREAD code are certainly much less than CR1, since the spreading behaviour for such releases will be dominated by the freezing-immobilization phenomenon and the melt leading edge may not even reach the liner. It is interesting to note that for the scenario 1 case 1.7 in table 4.1, the low superheat melt freezes within the pedestal, if the criterion for melt movement is restricted to less than 50% solid fraction.

The objective of the work in Part IV was to resolve the questions about the pile up of the melt on the liner region facing the pedestals doorway. I believe, that has been accomplished successfully, and it has been shown that the CR1 is slightly conservative for scenario 1 type releases and greatly conservative for the scenario 2 type releases. Additional insights have been obtained, in particular, that the melt in scenario 2 type releases may not even reach the liner and thus pose no threat of early containment failure. However, by the same token, the melt depths in the pedestal region may be large, perhaps greater than the water depth and there may be questions about the coolability of the melt and about the long term attack on the basemat. It should be recalled that the APRIL code analysis in Part II favors the scenario 2 type releases from the vessel.

Part V

"Creep-Rupture Failure in a Mark-1 Drywell Liner" by J. Castro, R. Dameron, J. Maxwell and Y. Rashid ANATECH Research Corporation San Diego CA. and D. Powers, Sandia National Laboratories, Albuquerque, N.M.

The work reported in Part IV performs a three-dimensional finite element creep rupture analysis of the Mark-1 liner for a localized contact of the core melt. The objective was to determine the rupture temperature if the liner is heated to 1300° C over a 10 minute interval in the region of contact with the core melt. This work was initiated in response to the criticism by the peer reviewers of the choice of 1500° C (melting temperature of steel) as the failure temperature of the liner in the NuREG/Cr-5423.

The finite element analysis was performed with the ABACUS code, which is a state of the art code of great maturity and, with the number of elements selected in the calculation, should be providing excellent results for stresses experienced in the local region of the liner in contact with the melt. In this context, it is unfortunate that a part of the analysis was performed in two dimensions which implies that the whole lower part of the liner (upto about 20 cms height) was heated to 1300° C in 10 minutes. This was certainly not the case and the two-dimensional results should not be considered in the failure temperature prediction.

The difficult part of this analysis is not the structural calculations or even the creep rupture calculations. It is the specification of a material behaviour model valid for high temperatures and large strains and the criteria for failure. It is on these two areas that the authors spent a considerable fraction of their total effort in this work. The material

model is based on the yield stress and elastic modulus versus temperature data obtained by combustion engineering, EPRI and INEL, and the constant load creep data obtained by INEL. The failure criteria are (a) an accumulated damage index and (b) total strain; the failure is predicted when the strain rates are in the range of 8 to 18% and the accumulated damage index is larger than 1. The accumulated damage index is based on the time history of the Larson Miller parameter.

I believe the authors have constructed a logical scheme for determination of the creep-rupture failure temperature of the liner at the region of contact with the melt. The three-dimensional calculation models the restraint exerted on the deformation of the locally hot liner region and obtains the creep rupture failure temperature of 1260° C. Such a high temperature is greater than the usual experience base of structural engineers in the one- and two-dimensional situations they generally deal with.

It is not clear to me why the failure criterion restricts the strain to ~18% at such high temperatures. One of the members of the working group, Mr. Bandyopadhyay, has commented that much larger strains are obtained without failure, when steel is forged at the temperatures considered in this analysis. Perhaps, the authors could consider this comment and evaluate the possibility of non-failure at strains of ~ 18%, when temperatures close to 1300° C are reached.

The objective of determining the creep rupture failure temperature for the liner has been met by the work documented in Part V. The authors have developed a logical method to obtain this result. Perhaps the authors should consider a larger strain than they have employed for creep-rupture failure of the locally hot region based on the data from the metal-forging industry.

Part I

"The Probability of Mark-1 Containment Failure by Melt-Attack of the Liner" by T. Theofanous and H. Yan.

The work reported in Part I integrates the results obtained in Parts II, III, IV and V into the ROAAM methodology to obtain modified or refined estimates of the liner failure probability due to melt attack in the presence of water. In addition, it also provides a scoping estimate of the steam explosion loads that are needed to damage the pedestal wall and concludes that the integrity of the pedestal will not be jeopardized.

The modification in the original estimates of the liner failure probability occurred due to the lowering of the liner failure temperature from 1500° C to 1260° C. The result obtained after combining this new value with the pdf's and the causal relations of NuREG/CR-5423 was 3×10^{-3} , which still falls in the realm of physically unreasonable. Reducing the failure temperature further to 1200° C but using the CORCON-3 based CR-2, resulted in the liner failure probability of 2×10^{-3} . I think that we can conclude that with water present the Mark-1 liner conditional failure is physically unreasonable to expect.

Prof. Theofanous has also estimated the pressures in a steam explosion that would be required to damage the pedestal walls. The shallowness of the water pool helps to "vent" the pressure pulse in the vertical direction and the horizontal propagation does not occur. The conclusion that, even if a steam explosion occurs in the melt-pouring mode, the damage to the pedestal walls will be minimal is very reasonable.

The discussion on the extension of the results of NuREG/CR-6025 to other Mark-1 plants is quite brief, since the IPE work and NRC's guidelines should be responsible for that. In general, the remark that, if the water depth (as a function of melt volume) is sufficient, the results of these studies should apply is reasonable.

Prof. Theofanous has commented on the work in Parts II to V and his comments are fair and well thought-out.

UNITED STATES
NUCLEAR REGULATORY COMMISSION
WASHINGTON, D.C. 20555

JUN 17 1993

MEMORANDUM FOR: Brian W. Sheron, Director
Division of Systems Research
Office of Nuclear Regulatory Research

FROM: Warren Minners, Director
Division of Safety Issue Resolution
Office of Nuclear Regulatory Research

SUBJECT: DRAFT NUREG/CR-6025 REPORT "THE PROBABILITY
OF MARK I CONTAINMENT FAILURE BY MELT-ATTACK
OF THE LINER"

We have reviewed the draft NUREG/CR-6025 report "The Probability of Mark I Containment Failure By Melt-Attack of the Liner" and have the following comments.

Part I of the report is a summary of the independent evaluations (Part II through V) and their effect on the assumptions and conclusions presented in NUREG/CR-5423. The report states on page 1-10 that "liner failure in Scenario II is physically unreasonable." On page 1-13, the report states that the probability of liner failure for Scenario I "clearly translate[s] to a 'physically unreasonable' event." These statements lead the reader to conclude that liner melt-through is physically unreasonable. This is true only for those sequences where the drywell (cavity) is flooded, as discussed in Part IV on page 1-8 and not for sequences where the drywell is dry. This fact should be clearly stated in Part I.

A discussion of melt spreading in a dry drywell, or a reference to another report, should be included to provide a complete report of all considerations related to liner failure. This additional information should include the release rates and volumes for Scenario II where the release freezes before reaching the containment wall.

Part II (page 29) of the report states that while "corium solidification and crust/molten-corium-pool formation are modeled in APRIL.MOD3," APRIL predicts "the core heatup process [to] occur rather uniformly, and the various molten material [to] gradually relocate along the lower portions of the core and fall into the water in the lower plenum (prior to reactor vessel failure) or are released into the containment (after vessel breach)." This is the description of Scenario II. This seems to say that TMI-type core melt progressions (Scenario I) are not credible for BWRs. If this is the finding, then this is important and is worth mentioning in Part I.

Part II also concludes that releases from the reactor vessel for Scenario I's, with or without melting of the upper structures (steam separator and steam

dryer), will be at least 50% metallic. This seems to be an important conclusion and is worth mentioning in Part I.

Finally as an editorial comment, page 1-14 refers to "Peach Bottom NNP." This is not a correct reference; it should be "Peach Bottom APS" for Atomic Power Station.

A handwritten signature in cursive script, appearing to read "Warren Minners".

Warren Minners, Director
Division of Safety Issue Resolution
Office of Nuclear Regulatory Research

UNIVERSITY OF CALIFORNIA, SANTA BARBARA

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DEPARTMENT OF CHEMICAL AND NUCLEAR ENGINEERING
CENTER FOR RISK STUDIES AND SAFETY

SANTA BARBARA, CALIFORNIA 93106-1070
(805) 893-4900
(805) 893-4927 FAX

July 15, 1993

Dr. Sudhamay Basu
Division of Systems Research
U.S. Nuclear Regulatory Commission
5650 Nicholson Lane
Rockville, MD 20852

Dear Dr. Basu:

Attached please find our responses to the peer committee comments referring to Part I of the Mark-I report

Sincerely,

A handwritten signature in black ink, appearing to read "T.G. Theofanous", with a long horizontal flourish extending to the right.

T.G. Theofanous, Professor
Department of Chemical and Nuclear Engineering
Director, Center for Risk Studies and Safety

TGT/h

Attachment

Response to R.E. Henry

Two concerns were raised by this reviewer on Part I of NUREG/CR-6025. Both refer to aspects peripheral to what actually has been addressed and quantified in this work, and the reviewer's point is that such comments made in passing run contrary to the disciplined character of the ROAAM. We agree in principle, but a few words of explanation may be in order.

1. About the very special case of low-positioned downcomers that may allow melt running into them, we offered a judgment in passing that reflects our understanding developed during the course of this work. This means that in our estimation evaluating such a geometry in the manner done in this report would produce a non-negligible failure probability.

There may be margins in this answer, but it would be, again in our judgment, a significant additional effort to find and quantify them. Since we were requested by the NRC to address this point in the report, our approach to resolving this issue was to add to the text, making it more clear that what we offer is our judgment in the sense described above.

2. The evaluation of steam explosion loads discussed in Appendix A was made at the request of the NRC. It was put in the Appendix as a separate item, to emphasize that it is outside the main field of evaluation. We thought that the steam explosion mechanism was so far from providing a challenge to the Mark I containment, that it might not require a detailed in-depth evaluation. So, Appendix A is nothing more than our reasoning for such a conclusion. To resolve this issue we have added to the text of the appendix to make more clear the purpose and the implied limitations of this appendix.

Response to S. Hodge

The numbers below refer to the numbers under Part I of the review comments.

1. As explained in section 4 of Part I the downcomer height, rather than its azimuthal position with respect to the pedestal door, is the important quantity. See also response under Part V.
2. See Table of Contents and actual Addendum (now called Appendix I) at the end of the report.
3. Change made.
4. See response under Part III.
5. Concerning the presence of a downcomer, see response under item 1 above. The interpretation of failure temperatures offered here is correct and consistent with our quantification. The 1260 °C is characterized as "conservative best estimate," which means a best estimate "somewhat" biased on the conservative side. See also response under Part V.

6. See response under Part III. Note that if both the CR2 and CR3 are taken at the 95 percentile we are already beyond what might be considered "reasonably conservative," not counting additional conservatism in pdf1, pdf2 and pdf3.
7. For the reasons explained above and in Parts III and V, we believe such a case would be unduly conservative.
8. On the configuration aspect, see item 1 above. The guidelines were developed by the NRC staff, and we do not believe it is appropriate to discuss them in any detail in this report.
9. The height, rather than the azimuthal position of the downcomer in relation to the pedestal door, is the key factor concerning the possibility of melt spilling into it. This is why we are focusing on height. Having the azimuthal position is fine, but unless it is more than, say, 30° from the door-to-shell normal it is not clear how such knowledge would help. If there are 8 downcomers in total, as is usual, this angle would be only 22.5°.
10. The proviso of low pressure scenarios has been adequately emphasized. The condition of 29.4 psia for doing the structural analysis is a consequence of accident evaluation, and the result is not sensitive to it (see Part V), so any additional emphasis would be confusing. The azimuthal position of the vent pipe in relation to the door is not germane, provided the height from the floor is adequate, as discussed already.
11. It is unlikely that an explosion of the type considered here could raise the pressure above 29.4 psia, in the presence of sprays, but, more importantly, the timing of such an explosion would be well before the liner could heat up to temperature levels at which such pressurization could be significant in the context implied here. This is why Appendix A focused on impulse loading.
12. All typos have been corrected.

Response to M. Kazimi

No issues for Part I of the report were raised by this reviewer.

Responses to M. Modarres

The numbers refer to those found in the review comments.

1. There is nothing particularly difficult about accounting for dependencies in the framework and corresponding calculations, when such dependencies exist. One such example and its treatment can be found in an early application of ROAAM (before the methodology

had a name) on in-vessel steam explosion energetics (Theofanous et al., 1987*). In the present case, the main dependencies arise along the lines of various scenarios, so they are accounted for through the definition of the "splinter" scenarios. As explained previously responding to this reviewer in NUREG/CR-5423, within each scenario any dependencies are overshadowed by other uncertainties, so a more sensible approach is to envelop the behavior by independently treated distributions is the three pdfs.

The suggestion about going into more detailed "prescriptions" of how various kinds of information is translated to a ROAAM quantification is a good one. We believe the best way is to forge ahead again with actual applications, as this one and the one just begun for Direct Containment Heating (Pilch et al., 1993**), as only they can truly reveal what coherence can be distilled from the wide multitude of phenomenology encountered in severe accidents. It would be an error in our opinion to spend time arguing about abstractions (such lend themselves particularly well to arguing!) while we could be spending our time solving actual problems. But the best example of the sort of difficulties run into by such "abstract" considerations can be found in the next item of this reviewer's comments and our response.

2. This comment addresses one of the key points of ROAAM, in which it significantly departs from current PRA practice. They (and this reviewer) say the aim should always be to define a best estimate behavior and an uncertainty around it. We say, sometimes a best estimate is so fraught with uncertainty that it is meaningless, and also we say, when the distributions are very wide, they can produce results that may be misleading and not particularly helpful toward reaching a resolution. We say all these problems can be eliminated by using conservatively biased distributions, as in ROAAM. We have demonstrated that experts can agree on such a basis and then the results become useful. The concept of using bounding estimates as a safety assessment tool is well-known and very useful. When one needs to back off from such bounding estimates (i.e., when one cannot take the consequences), this is when, usually, one ends up with "issues". We believe that ROAAM is the next best thing to absolutely bounding assessments for which we can easily agree on only two things: (a) how to do the calculation, and (b) the reality is much less severe!

Now, going to the specifics, the reviewer claims that we should use the CORCON.Mod3 CR2, or say what is wrong with it, for otherwise the "CR2 remains a divergent issue."

* T.G.Theofanous, B. Najafi and E. Rumble, "An Assessment of Steam-Explosion-Induced Containment Failure. Part I: Probabilistic Aspects," *Nuclear Science and Engineering*, 97, 259-281 (1987).

** M.M. Pilch, H. Yan and T.G. Theofanous, "The Probability of Containment Failure by Direct Containment Heating in Zion," Draft NUREG/CR-6075, SAND93-1535, June 1993.

Our position is that the convergence between the NUREG/CR-5423 quantification and the audit CORCON.Mod3 results is substantial, and furthermore, this comparison indicates that the former one is conservative. Based on this we believe that the FCE-1 and FCE-2 provide complementary and mutually reinforcing perspectives very useful for a safety expert knowledgeable in phenomena to judge the likelihood of liner failure.

Finally, on the probability levels in Table 2, the reviewer is *reminded* that, as emphasized in every place where our probability scale has appeared, the numbers employed in the scale are *arbitrary*. We use them only as intermediate tools to work out the framework, and the result is only stated by translating back to the meanings using the same table.

3. Strongly disagree with this comment. The reviewer failed to provide any support to his claim that our extrapolation results are premature and simplistic. The consideration of only low pressure scenarios was not on the basis that it is a good assumption for "Peach Bottom" and there should be no implication that it may or may not be a good assumption for any other BWR. In general, it is judged that low pressure scenario is the appropriate phenomenology for the extrapolation consideration, and more importantly, it is in this context that the liner meltthrough issue arises.

Response to F. Moody

No specific issues were raised for Part I by this reviewer. We simply would like to add our agreement to the reviewer's point that any special case(s) where the downcomer inlet is on the drywell floor can best be addressed in the IPEs, taking into account other plant-specific issues, such as core size, etc.

Response to D. Powers

Under item 1 of the comments the only comment about Part I is that we should have included the high pressure scenario case from the Part II results in our Table 3. We disagree. We emphasize everywhere in the report that we consider only low pressure scenarios, and such an inclusion in Table 3 would be really confusing, while adding really nothing of value. All the other discussion here refers to the work presented under Part II; hence we defer the response to these authors.

Item 2 refers mainly to Part V of the report; hence we defer the response to these authors. We wish only to remind the reviewer that the extend of spreading is not a parameter to be varied *at will*. Larger spread areas imply smaller depths and thus lower liner temperatures. The depths that are of concern for failure correspond to degrees of spreading less severe than what has been considered in Part V. As for the suggestion to consider cases intermediate to FCE-1 and

FCE-2 by changing one factor at a time, we have resisted the impulse to do so because, as we explain in the text, such cases would be unduly conservative. [Note, however, that the result of a quantification as in FCE-1 but with a failure criteria of 1200 °C was already included in NUREG/CR-5423.]

Now, referring to the reviewer's comment about Table 6 in Part I (his 4th paragraph under item 2), we point out that two results are possible: a single conservative value using the framework in the body of NUREG/CR-5423, and a probability distribution of failure frequency using the framework in Appendix I of the same report. As explained in Appendix I, to get to the probability distribution result you need more detailed information on failure criteria than provided in Part V. Thus we chose to use a single bounding value for failure temperature (the 1260 and 1200 °C for FCE-1 and FCE-2 cases, respectively).

Response to R. Sehgal

No issues for Part I were raised by this reviewer.

Response to W. Minners

That the low failure probability estimates refer to a flooded drywell was already abundantly clear in Table 1 of the report. Also, it was quite clear that the dry drywell case has been considered already in NUREG/CR-5423!

Finally, this reviewer confused case 8 considered in Part II with the scope of the present investigation. Case 8 refers to a high pressure scenario, and here we are concerned with low pressure scenarios only.

Rensselaer

Department of Nuclear Engineering & Engineering Physics

July 15, 1993

Dr. Brian W. Sheron
Division of Systems Research
Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
Washington, D.C. 20555-0001

Dear Dr. Sheron:

Per your request of July 2, 1993 I have reviewed the Peer Review Comments on the draft report entitled "The Probability of Mark-I Containment Failure by Melt Attack of the Liner" (NUREG/CR-6025)). My response to the reviewers' comments in regard to the part performed at RPI (Part II) is attached to this letter.

Sincerely,

A handwritten signature in cursive script that reads "Michael Z. Podowski".

Michael Z. Podowski
Professor and Department Chair

cc: T. Theofanous
S. Basu

Response to the Reviewers' Comments on the Report entitled, "The Probability of Mark-I Containment Failure by Melt-Attack of the Liner; Part II - The Effect of Heatup and Meltdown of Reactor Vessel Upper Internals on the Composition of Corium Discharged into the Drywell of a BWR Mark-I Containment".

Comments by R.E. Henry.

The reviewer found the APRIL results reasonable. No specific comments or suggestions were given.

Comments by S.A. Hodge.

As stated by the reviewer, he transmitted his comments to the authors of Part II at the time where the first draft of this document was prepared. All such comments were taken into consideration in the final revised version of the report. The reviewer's statement concerning the work objective is formulated properly.

Comments by M.S. Kazimi.

The reviewer presented additional comments in support of the conclusions drawn in the report. Whereas he considers the comments by V. Denny to be well taken, the final conclusion is that "the melt would remain mostly metallic", as stated in the report.

Comments by M. Modarres

No specific comments on Part II were found.

Comments by F.J. Moody

The reviewer confirmed the report's findings. No other comments were found to be explicitly referring to Part II.

Comments by D.A. Powers.

The reviewer discussed the comments by V. Denny. He concluded that the suggested (by Denny) chemical dissolution of oxidic corium is not an important effect. Other aspects of Denny's comments are discussed in regard to Part I rather than Part II.

Another issue raised by the reviewer is concerned with the comments by R. Schmidt and with the impact of restrictions (such as system depressurization) on the overall conclusions. It should be reiterated here that since the low pressure melt release conditions in the APRIL calculations were a part of the official problem formulation, only one high pressure scenario was included for the purpose of comparison. Whereas the results for this case are still within the bounding assumptions in NUREG-CR, their limited scope did not allow for drawing more general conclusions.

Comments by B.R. Sehgal.

In the first part of the review, Dr. Sehgal offered a few specific comments concerning the results of APRIL. As he properly observed, the predictions of in-vessel melt-water interactions and lower head failure by existing severe accident codes are still associated with substantial uncertainties. Because of that, the APRIL calculations have been performed parametrically in order to quantify

these uncertainties on the one hand and to maintain reasonable margins of conservatism on the other hand. The effect of radiative heat transfer has been explained in the report in detail, including estimates based on simplified stand-alone calculations. Due to the lack of direct experimental evidence, the validation of natural circulation inside the vessel was mainly based on parametric testing and numerical verification of major modeling assumptions. All these testing and cross-comparisons confirmed the overall consistency of the model. Although several uncertainties still do exist, it is believed that the order of magnitude of natural-convection driven flows inside the reactor vessel has been properly captured.

In other comments, the reviewer agreed that the issues raised by V. Denny did not change the overall conclusions in Part II concerning the composition of corium discharged to the containment. A similar conclusion was drawn in regard to the comments by R. Schmidt.; specifically, whereas the possibility of large releases of oxidic corium cannot be precluded, according to Dr. Sehgal the probability of such events should be much smaller than the probability of those predicted by APRIL. Thus, the reviewer basically agreed with our findings.

Comments by W. Minners.

The reviewer's comments were mainly on the importance of explicitly mentioning the conclusions from Part II in Part I of the report. No specific comments or recommendations were found in regard to the methodology or results in Part II.

Sandia National Laboratories

Albuquerque, New Mexico 87185

July 12, 1993

Professor T. G. Theofanous
Department of Chemical and Nuclear Engineering
Director, Center for Risk Studies and Safety
University of California, Santa Barbara
Santa Barbara, CA 93106-1070

Re: Response to Reviewer Comments Concerning NUREG/CR-6050 Part III

Dear Theo,

Enclosed are responses I have prepared to reviewer comments concerning "Audit Calculations with CORCON-MOD3 of the Duration of Superheat in NUREG/CR-5423."

I believe I have responded to all statements that can be construed as comments or recommendations. Necessary changes to the text of this chapter in the overall report have been made.

Sincerely yours,

Dana A. Powers, Manager
Integration of NRC Work Program
Support Department, Org. 6404

DAP:6404:smu

cc: Dr. S. Basu, NRC

Responses to Comments Concerning
AUDIT CALCULATIONS WITH CORCON-MOD3
OF THE DURATION OF SUPERHEAT
IN NUREG/CR-5423

D. A. Powers

Sandia National Laboratories
Albuquerque, New Mexico

Comments from several reviewers were received concerning the document "Audit Calculations with CORCON-MOD3 of the Duration of Superheat in NUREG/CR-5423," Part III NUREG/CR-6050, by D. A. Powers and T. J. Heames. Below are presented the authors responses to these comments.

Reviewer: R. Henry
Fauske and Associates, Inc.

Comment:

"I have reviewed the CORCON-MOD3 results. These provide the necessary audit calculations for the approach used in NUREG/CR-5423."

Response:

The authors agree.

Reviewer: S. A. Hodge
Oak Ridge National Laboratory

Comment:

"My general impression after reading Part III is that while the results of this work certainly do not pose any challenge to the duration of superheating utilized in NUREG/CR-5423 analyses, they also do not provide any basis for reducing (reviewer's emphasis) the period of superheat duration used in the study."

Response:

The reviewer reiterates in the complete text of his comments caveats listed in Part III concerning CORCON predictions of the duration of superheat. The authors stand by these caveats concerning the limitations of CORCON predictions. The authors also stand by their conclusion that to a very high confidence level durations of superheat used in

NUREG/CR-5423 are conservative relative to what is predicted by CORCON. Unfortunately, we are here in the business of comparing two computer code predictions to each other and not comparing predictions to experimental data for the precise circumstance at issue. We note, however, that in comparisons to other experimental situations, such as the large scale tests of $\text{UO}_2\text{-ZrO}_2\text{-Zr/concrete}$ interactions, that CORCON has predicted with adequate accuracy melt temperature during the first tens of minutes of interaction which would be pertinent to the issues addressed in NUREG/CR-5423. Nevertheless, the only basis for asserting that the CORCON code predictions justify reduced durations of superheat is that CORCON has received a great deal more attention, scrutiny, and validation than the code used for the original NUREG/CR-5423 predictions. It might be argued then that predictions obtained with CORCON are better "best estimates" within the current state of technology.

Recommendation #1:

The authors should provide a clear statement as to whether or not they believe the results of their work justify any reduction in the period of superheat duration as utilized in NUREG/CR-5423.

Response:

The authors do not feel they have the responsibility to do this. This is the task for the probabilistic synthesis in Part I. The authors were consulted by those doing the probabilistic synthesis and enthusiastically support the use made of the CORCON results in the probabilistic synthesis.

Recommendation #2:

The first paragraph of Section 3 (page 6) should be revised to also point out that Scenario I is the case most threatening to the drywell shell.

Response:

The authors thank the reviewer for this suggestion and have made an appropriate change in the text.

Reviewer: M. S. Kazimi
Massachusetts Institute of Technology

Comment:

The results of the independent investigation using CORCON-MOD3 indicate that the values used for superheat duration in NUREG/CR-5423 were conservative. Only one case out of seventeen shows CORCON predicted a duration slightly larger (sic) than that used in the original report. The only cautionary remark I have about this set of calculations is the well known uncertainty about the CORCON results in the initial few minutes (2-3) of a calculation.

Response:

The authors believe an adequate description of the uncertainties in the CORCON results have been included in Part III.

Reviewer: F. J. Moody
G. E. Nuclear Energy

Comment:

The corium-concrete interaction, strongly dependent on melt superheat, was found to be conservatively addressed in the ROAAM application, based on CORCON-MOD3 superheating predictions.

Response:

The authors agree.

Reviewer: B. R. Sehgal
Royal Institute of Technology

Comment:

The documentation does not provide proper information about the liquidus curve used in the calculations. Figure 1 shows the curve without any values on the ordinate of or any values on the abscissa of the mole fraction of concrete in oxide core debris.

Response:

The liquidus curve used in CORCON MOD3 as well as all other technical details of the code are described at length in the code user's manual and open literature publications. The schematic diagram in Figure 1 was used to describe the concept of "superheat" precisely. This discussion was appreciated by some reviewers.

Comment:

Additionally, the CORCON subroutine calculating the liquidus curve uses ideal solution chemistry and that has been questioned by M. Roach of ANL.

Response:

The reviewer is in error. The liquidus curves are calculated from fits to phase relation data and do not rely on ideal solution approximations.

Comment

From an early version of the draft report "Liquidus-Solidus Temperatures for Core-Concrete Mixtures" by Roache et al., the liquidus measured for a mixture of 27.5 wt % limestone-sand concrete with $\text{UO}_2\text{-ZrO}_2$ at a UO_2 to ZrO_2 ratio of 1.625:1 is quoted as $>2365^\circ\text{C}$. I believe this value is larger than would be calculated in CORCON-3; however, a comparison should be made and shown in the report.

Response:

The authors first note that the work by Roache et al., was not available at the time the audit calculations were done. In fact, this work has only recently been published. Second, the authors note that incorporation of 27 w/o of concrete into the core debris

would not occur until long after the period of interest for the calculations in NUREG/CR-5423. A more useful comparison for this work is the comparison of CORCON predictions to the liquidus of the $\text{UO}_2\text{-ZrO}_2$ system which is matched exactly. Third, if CORCON predicts liquidus temperatures that are less than measured this would only mean that CORCON predictions of the duration of superheat are themselves conservative. Many factors go into the calculation of the duration of superheat aside from phase relations in the oxide system. Validation of CORCON predictions of these factors is well-beyond the scope of Part III. In fact, such information is to be found in the User's Manual and in the CORCON validation study--both of which are rather lengthy documents available to the reviewers.

ARGONNE NATIONAL LABORATORY

9700 South Cass Avenue, Argonne, Illinois 60439

July 12, 1993

Professor T. G. Theofanous
Professor and Director
Center for Risk Studies and Safety
University of California
Santa Barbara, CA 93106-5080

Dear Theo:

As requested by Sud Basu, I am hereby sending you our responses to the review comments concerning Section IV of NUREG/CR-6025 documenting the MELTSPREAD-1 code calculations. I have also enclosed revised pages from Section IV containing the modified text. Please feel free to call me if you have any questions or suggestions for further enhancements.

Comments of R. E. Henry: Dr. Henry's comments are elaborative in nature and do not require modification of Section IV.

Comments of S. A. Hodge: The first comment relates to the consistency of the drywell pressures assumed in Sections IV and V. The 0.3 MPa value used in Section IV came from the output of a BWR SAR calculation for a short term station blackout in Peach Bottom provided to Argonne by Dr. Hodge. The source for this value is now noted and referenced in Section IV as a private communication from S. A. Hodge. As an aside in the present letter, it is to be noted that some analyses have provided values of about 0.2 MPa which was assumed in Section V. For instance, see the IDCOR Program Report, "Technical Report 23.1PB Peach Bottom Atomic Power Station Integrated Containment Analysis," The Industry Degraded Core Rulemaking Program (March 1985).

The second comment is a recommendation that the sentence, "According to Professor Theofanous, all other U. S. Mark I units have similar pedestal wall and liner diameters while the total sump volumes of the in-pedestal sump pits in all other U. S. Mark Is are greater than or equal to the Peach Bottom sump volume," be removed. This statement has been removed from Section IV.

Comments of M. S. Kazimi: The comments of Dr. Kazimi include the question, "What is the effect of completely neglecting the heat transfer to water or having the water at a higher temperature than assumed here?" I assume that completely neglecting heat transfer to water refers to the dry case which was deliberately not included in the scope of the MELTSPREAD-1 analysis which addressed the wet case. The effect of water subcooling

(taken to be 50 K) was not examined. To do so would require additional calculations assuming lesser subcooling for each of the two scenarios. As Dr. Kazimi points out, this effect was indirectly addressed by those cases in which the upward and downward heat transfer coefficients were reduced by 50 percent. Thus, no modifications to Section IV regarding the water temperature effect have been carried out.

Comments of M. Modarres: There are no specific comments regarding Section IV.

Comments of F. J. Moody: No modifications of Section IV are suggested by Dr. Moody.

Comments of D. A. Powers: There are no comments on Section IV.

Comments of B. R. Sehgal: Dr. Sehgal found some Scenario II results difficult to understand (e.g., Cases IIA3 versus IIA4 and IIA3 versus IIA1) and suggested that perhaps some of these differences should be explained. What we are seeing here is the effect of melt immobilization as a layer short of reaching the liner such that the depth increases abruptly as the extent of spreading increases to encompass nodes adjacent to the liner. Discussion of this effect has been added.

Comments of W. Minners: There are no comments on Section IV.

Sincerely,

James J. Sienicki
Group Leader, Engineering Analysis
Engineering Development Laboratories
Reactor Engineering Division

JJS:ljo

cc: F. Eltawila, NRC
C. E. Tinkler, NRC
S. Basu, NRC

Responses to Reviewer Comments on

NUREG/CR-6025 PART V

**"CREEP RUPTURE FAILURE IN A MARK I
DRYWELL LINER"**

Y. R. Rashid

D. A. Powers

August 26, 1993

Comments from several reviewers were received concerning "Creep Rupture Failure in a Mark I Drywell Liner," NUREG/CR-6025, Part V. Responses to these comments are presented below.

Reviewer: R. Henry

Comment: "As the wall is calculated to deform the debris crust is assumed to move with the wall instead of opening a gap which would slow the energy transfer rate."

Response: The temperature history of the liner exposed to molten core debris was obtained from analyses by the authors of NUREG/CR-5423. Details of the heat transfer modeling they used are described in NUREG/CR-5423. Formation of a gap between solidified core debris and the liner can occur without deformation of the liner and has been observed in experiments conducted by one of us (DAP). Similarly, these experiments showed the formation of an area of contact resistance due to carbide formation. All of these things have to be addressed to obtain an accurate estimate of the liner temperature. These are, however, not topics addressed in Part V of NUREG/CR-6025.

Additional expansion of the gap between the debris and the liner as a result of creep could be addressed only by doing a coupled calculation of heat transfer and structural response. Such a calculation was well beyond the capabilities of the project. Neglect of an expanding gap represents an appropriate conservatism of the analyses done here and text has been added to indicate this.

Comment: "... one should expect a significant contact resistance due to the vaporization of water in the steel surface cavities or due to the burning or melting of paint on the liner. This would slow the entire thermal transient and reduce the peak wall temperatures for the same initial conditions."

Response: Again, the temperature history for the liner was simply an input to the analyses in Part V. This history was obtained from NUREG/CR-5423. Creep is, of course, a function of both time and temperature. A slower heating scenario might well lead to rupture at lower temperatures because there would be time for more extensive deformation of the steel at low temperatures. It is, then, not evident that neglect of contact resistances cited in the comment represent important conservatisms in the analysis.

Reviewer: S. A. Hodge

Comment: "... the ANATECH work was based upon a drywell configuration in which the pedestal doorway does not face a vent pipe opening, which is not true of Peach Bottom. (It is true of Brown's Ferry)."

Response: The comment on the neglect of the downcomer is accurate. Inclusion of the downcomer would have complicated the analyses beyond the capabilities of the project. We cannot agree, however, that the neglect of the downcomer makes the calculations non-conservative.

Estimating the effects of downcomer penetrations on the creep rupture behavior of the liner is difficult. The downcomer in the vicinity of the region of debris contact with the liner would certainly affect the maximum temperature and the temperature distribution in the liner. The geometric discontinuity presented by the downcomer can be the cause of stress/strain concentration or stress/strain relaxation. The most critical issue is the elevation of the downcomer relative to the height of the debris. If the downcomer is so high or displaced so far from the debris that it does not affect the temperature or stress distributions such as those shown in the report, then the effect of the downcomer should be negligible. This seems to be the case both for the Brown's Ferry units and for Peach Bottom. This geometrical feature varies among the various Mark I containments.

Consequently, this plant specific feature would have to be taken into account when applying the results obtained in NUREG/CR-6025 in examinations of other plants.

Comment: "Second, I do not believe that a typical reader of Part V would consider the stated finding of 1260°C to be "conservative." Rather, I believe that Part V indicates that a failure temperature of 1200°C should be utilized, if one desires a 95% probability that the actual failure temperature would not be lower than the temperature utilized. Thus, use of 1200°C would be "conservative."

Response: This failure temperature satisfies both the accumulated damage index criterion and a somewhat conservative interpretation of the creep ductility exhaustion criterion. That is, the uniaxial failure limit at 1260°C involves strains of 48%. There is a substantial scatter in the measured failure strain data. Consequently, a value for the rupture strain one standard deviation less than the mean was selected for the rupture strain criterion. The one standard deviation uniaxial failure strain is about 35% = 48% (1 - σ). Application of the biaxial factor, 2, yields a one standard deviation failure strain of 17.5% at 1260°C. Thus, the failure strain criterion used in the report is somewhat conservative. This approach was taken to account for some of the other uncertainties not considered explicitly in the calculations such as flaws in the steel. The uncertainty analysis presented in Chapter 3 examines the accumulated damage index criterion. A distribution in the temperature at which the damage index exceeds 1 is found from uncertainties in the correlation of measured values of the Larson-Miller parameter. Since both the strain failure and the damage index criterion must be satisfied, additional conservatism can be incorporated into the analysis by considering the uncertainty distribution for the damage index criterion. Based on this uncertainty analysis 1200°C is more of a bound than 95%. 1238°C would be the 95% bound if 1260°C is taken to be the 50% percentile value.

Comment: "The statement that the assumption that the creep properties of the steel above 1100°C are the same as those measured at 1100°C 'probably is a non-conservative approximation' is somewhat unsettling. It would be desirable for the authors to provide some estimates as to the potential impact of this."

Response: At temperatures above 1100°C, the yield stress may be reduced considerably. Creep strain rates for comparable stresses may increase. On the other hand, a lower creep rate retards stress relaxation in the confined hot region so higher stresses are maintained for longer times. Since the stress dependency of the creep rate is stronger than the temperature dependency, we believe the net effect of treating creep rates above 1100°C as the same as creep rates at 1100°C to be small. These creep rates with the 10-20% failure strain criterion seem appropriate in the absence of additional data.

Comment: "The penetrations of the liner by downcomers ... are not modeled in the three-dimensional analysis. One of the Peach Bottom downcomers is located directly opposite the pedestal doorway. It seems that the presence of this downcomer must affect the calculated creep rupture failure temperature and the report should provide at least a qualitative discussion of what this effect would be."

Response: This comment reiterates an earlier comment. Again, analyses including the effects of the downcomers would be most challenging. Downcomers that affect the temperature or stress distributions might raise or reduce the failure temperature depending on how they affect temperatures in the steel and whether they act to concentrate or relax stress. The variability in the location of the downcomers in Mark I boiling water reactor drywells is a plant specific feature that would have to be taken into account in the extrapolation of results obtained in NUREG/CR-6025 to other Mark I designs. It is our judgment that the penetration for the downcomer is sufficiently elevated above the drywell

floor (30cm) and the region of debris contact with the liner that it will not significantly affect the stress and temperature fields in the liner.

Comment: "... that the analysis is based upon a constant 0.2 MPa (29.4 psia) pressure. In my opinion, the pressure may very well be higher than this and I would like to have the author's estimate as to the effect upon the creep rupture failure temperature results if a pressure of say 43.5 psia (0.3 MPa) as was used as the basis for the Part IV Melts spread calculations had been employed (instead of 29 psia) for the creep rupture analysis."

Response: At the time the analyses in Part V were initiated, 0.2 MPa was the best estimate of drywell pressure provided by the authors of NUREG/CR-5423. Increasing the drywell pressure will decrease the failure temperature. The effect would be easy to estimate if the loading on the liner were uniaxial. Increasing the stress by 50%, keeping failure times the same, would decrease the failure temperature by 60 to 70°C. In a confined three dimensional structure, however, the effect could be much smaller because increased stress relaxation in the confined hot region and redistribution of stress to the colder regions.

Comment: "Figures 20 and 21 and Table 2 seem to indicate that if an assumed failure temperature was desired such that there was a 95% probability that the actual failure temperature was not lower than the assumed value, then this assumed failure temperature would be about 1200°C cited in the Abstract and in paragraph two of the conclusions (Page 40) is such that there is a 95% probability that the actual failure temperature is not higher than this. If this is true, then a value of 1200°C, not 1260°C should be used in the Part I analyses."

Response: The reviewer is correct in his interpretations of the uncertainty distributions. Analyses are presented in Part I for both 1260°C and 1200°C as failure temperatures. As indicated above, 1260°C is our estimate of the failure temperature whereas 1238°C is our 95% confidence bound. That is, we are 95% confident that failure would not occur at a temperature lower than 1238°C for the conditions assumed in the analyses.

Comment: "I recommend that the basis for the creep rupture temperature of 1260°C as stated in the Abstract and Conclusions of Part V be clearly explained in relation to the failure temperature distributions in Section 3.0."

Response: Text to clarify the relationship has been added.

Reviewer: M. S. Kazimi

Comment: "It should be mentioned that the data used ends (sic) at 1100°C. For higher temperatures, the behavior was assumed similar to [that] at 1100°C. As pointed out by the BNL reviewers, at higher temperatures larger strains may still be possible, as demonstrated by steel forging practices."

Response: First, we caution against over-interpretation of strains observed in uniaxial forging. They cannot be directly applied to the biaxial behavior of the liner. Next we note that had creep rates been allowed to increase with temperature beyond 1100°C, the analysis would have yielded larger strains. These larger strains would have been compared to a higher failure strain criterion. The larger strains compared to a higher failure strain criterion probably would not change the failure temperature significantly.

Comment: "The integration of the results in Part I is done at two temperatures: 1260°C, the recommended failure criteria (sic), and 1200°C, as a bounding criterion. The extra measure of conservatism is prudent given the differences in the uncertainty in scenarios of melt attack on the liner."

Response: Agreed.

Comment: "Furthermore, while the ANATECH results are for a 20 cm deep layer, the synthesis applied the same failure criterion for shallower depths of 15, 10, and 5 cm. This will add conservatism."

Response: The reviewer is correct. Two effects have been considered here. For a fixed volume of melt, more spreading of the melt will produce shallower melt pools and consequently lower peak temperatures in the liner. Lower temperatures make it less likely that the creep rupture failure criteria developed in Part V of NUREG/CR-6025 will be exceeded. On the other hand, a greater arc of liner will be in contact with the melt and the heating rate of the liner will be slowed. Both of these factors would make the liner more susceptible to creep rupture failure. However, even in the very conservative two-dimensional case a complete spreading of debris over the entire floor to a depth of 20 cm the rupture temperature fell only by 200°C. Peak liner temperatures are more sensitive to melt depth so it appears less likely that shallow pools could heat the liner enough to exceed the creep rupture failure criteria.

Comment: "I think the curves shown in Figure 8 have a reversed order of melt height."

Response: Figure 8 does not have any indication of melt heights--it refers to liner deformation. All figures seem to be correct.

Comment: "Another point to note is that while the behavior assumes a drywell pressure of 0.2 MPa, that pressure is easily transmitted to the back of the liner via the existing large openings above the melt location."

Response: The comment is in error. If pressure is transmitted to the backside, the liner has failed! It is not at all clear what is meant by Prof. Kazimi's comment.

Reviewer: F. J. Moody

Comment: "Finally, the liner failure criteria work of ANATECH resulted in lowering the NUREG/CR-5423 temperature from 1500°C to 1260°C for a conservative interpretation. The topic of creep rupture is not my expertise, although the arguments seem reasonable, leading to conclusions which still lie within the bounds that in the presence of water, liner failure is physically unreasonable."

Response: The authors agree.

Reviewer: B. R. Sehgal

Comment: "... data from steel forging literature should be employed to determine the failure damage index and strain rate for liner temperatures of 1200°C and above ... "

Response: The authors are not aware of any useful, quantitative data beyond that which has been used and described in Part V. Even if data from the forging industry were available, such data must be used with care. Clearly, such data would be for a rather

different alloy. Such data would be for uniaxial creep. The liner analyzed in Part V responds to heat biaxially. The 18% creep criterion for biaxial creep used in Part V corresponds to 36% uniaxial creep which is consistent with creep strains cited by Bandyopadhyay in his review. Note also that we have deliberately chosen a strain failure criterion that is somewhat conservative (by one standard deviation) to account for other uncertainties such as flaws and corrosion. This is, of course, consistent with the standards of structural analysis. Also note that failure mechanisms and failure modes in a load-bearing structure are likely to be different than those in a designed manufacturing process which can take advantage of material restructuring.

Comment: "In this context, it is unfortunate that a part of the analysis was performed in two dimensions which implies that the whole lower part of the liner (up to about 20 cm height) was heated to 1300°C in 10 minutes. This was certainly not the case and the two-dimensional results should not be considered in the failure temperature prediction."

Response: Two dimensional analyses were done to explore the problem and to set the stage for the more complicated three dimensional analyses. Two scenarios were examined. One involved heatup to 1300°C over 10 minutes. The second involved heatup to 1300°C over 30 minutes. Results obtained in these analyses provide indication of how the creep rupture changes as the heating rate slows or the area of the liner in contact with core debris increases for a fixed depth of debris. Since it was possible to do only one three dimensional analysis for one temperature history and one contact area, some indication of sensitivities to these uncertainties is quite useful in contrast to the reviewers implications. Even in the limiting case of a slow (30 minute) heatup with a debris pool 20 cm deep, the creep rupture temperature fell only about 200°C. Results of the two-dimensional analyses have not been used in the probabilistic synthesis of NUREG/CR-6025.

Comment: "Perhaps the authors could ... evaluate the possibility of non-failure at strains of ~ 18% when temperatures close to 1300°C are reached."

Response: Responses to other comments above probably address this comment adequately. Based on the uncertainty analysis done in Section 3.0 of Part V, the authors are 95% confident that the creep rupture failure temperature is less than 1310°C for the heating and configuration considered taking 1260°C as the median (our best estimate). Once damage accumulation begins to approach an index value of 1, the damage accumulation grows rapidly. All indications from the present calculations are that even very non-conservative use of the two criteria for failure would lead to predictions of failure at temperatures less than 1310°C and certainly less than the melting point of steel.

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