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CLINCH RIVER

BREEDER REACTOR PROJECT

PRELIMINARY SAFETY ANALYSIS REPORT

VOLUME 17

PROJECT MANAGEMENT CORPORATION

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Question 001.246 (15.7.1.2.2)

Provide a list of "Perferred Directions" for all safety-related air-operated valves.

Response:

The information requested is provided in response to Question 001.245.

Question 001.247 (15.7.1.4.2)

Justify the statement that there will be no effect on reactor vessel sodium level with a cover gas pressure increase from 10" W.G. to 15 psig.

Response:

The reactor vessel, pump tank and reactor overflow tank are all connected with a gas equalization line which will maintain the pressures equal during this pressure transient. Levels, therefore, will not change.

Question 001.248 (15.7.1.4.2)

Provide the permissible leakage rate for the elastomer seal system at 15 psig.

Response:

Revised Section 15.7.1.4.2 provides the requested leakage rate for the elastomer seal system at 15 psig. 25

Question 001.249 (15.7.1.4.2)

Confirm that it is a design basis that IHTS pumps will operate with cover gas pressure at atmospheric, without cavitation.

Response:

The requested information is provided in revised Section 5.4.2.3.1. 25

Question 001.250 (15.5.1.5.8)

Provide the analyses supporting the value of a pump discharge pressure of 308 psig .

Response:

Section 15.7.1.5.2 has been revised to address this question.

Question 001.251 (15.7.2.1.2)

Provide an analysis of the reactivity effects of the maximum quantity of oil available in the system being introduced into the sodium coolant (assuming a release of hydrogen gas) and entering the core.

Response:

This analysis is in revised Section 15.7.2.1.2. 25

Question 001.252 (15.7.2.1.2)

Provide an estimate of the change in the plugging temperature, assuming a release of the maximum quantity of oil available into the sodium.

Response:

The response to Q001.251 gave information on this subject relating potential reactivity effects to hydrogen formation from reacting oil with the sodium coolant and is supplemented by the response given below.

Section 15.7.2.1 Inadvertent Release of Oil Through Pump Seals (PHTS) has been revised to reflect the latest pump concept. The discussion of causes and effects including the change in plugging temperatures is provided in the revision.

Provide an analysis of the sensitivity of the inert gas monitoring system for detecting oil leaks.

Response:

PSAR Section 9.8.2.2 identifies the location of the sampling point for the Primary Cover Gas Sampling and Monitoring System as near the reactor cover gas outlet nozzle. This location was chosen to reduce transit time and dilution effects on detection of fuel clad defects. Taking samples from this point, however, minimizes the monitoring system capability of detecting the H_2 which would be released should oil leak into a pump tank.

The primary means of detecting an oil leak is the oil inventory monitoring instrumentation which monitors the level of oil in the various pump seal oil tanks. The design of this system is not yet complete; therefore the sensitivity can not be specified at this time. However, as noted in Section 15.7.3.7, the entire contents of the 6 gallon oil off-leakage tank could leak into the PHTS sodium without impacting public health and safety.

Question 001.254 (15.7.2.4)

Provide the maximum permissible leak rate for the RAPS surge vessel cell.

Response:

Section 15.7.2.4 has been revised to include discussion of the RAPS surge vessel cell design leak rate. This leak rate will assure that in the event of a single vessel failure, the associated doses will be less than the limiting values of 10% of 10CFR 100. Testing provisions regarding the leak rate is also addressed.

Technical specifications regarding the maximum permissible inventory in the RAPS surge vessel are discussed in PSAR section 16.3.6.2.3. The technical specifications on (a) the RAPS surge vessel activity inventory and (b) cell leakage rate will assure the site boundary dose associated with a postulated vessel rupture will not exceed appropriate Federal Regulations.

Question 001.255 (15.7.3.1.2)

Provide the calculations supporting this section.

Response:

The text of PSAR Section 15.7.3.1.2 has been revised to incorporate the requested calculations. 25

Question 001.256 (15.7.3.1.2)

Justify the assumption that the assembly will retain its structural integrity.

Response:

Justification of this assumption is provided in revised Section 15.7.3.1.2. 25

Question 001.257 (15.7.3.1.2)

Justify the assumption that only fission gases are released on clad and limited fuel melting.

Response:

The justification for the above assumption is found in revised Section 15.7.3.1.2.

Also, the disposition of the fission products is discussed in the response to Question 001.212. As discussed there, only the Kr, Xe, and I activities contribute to the total off-site doses presented in Section 15.5.2.3. 25

Question 001.258 (15.7.3.4.1)

Provide an analysis of the consequences of a release of cover gas to the HAA.

Response:

The Head Access Area (HAA) atmosphere communicates freely with the upper RCB atmosphere. To provide a conservative, upper bound estimate of the potential consequences of postulated cover gas releases to the HAA, an instantaneous release of the entire primary system cover gas inventory to the RCB is evaluated. Such a release is considered hypothetical; its consequences are evaluated to demonstrate that even for this limiting case release, no danger to the health and safety of the public exists.

It is assumed that the entire primary system cover gas inventory (Reactor Cover Gas, Overflow Vessel Cover Gas, and PHTS Pump Cover Gas) is instantly released to the RCB. The cover gas activity used for the analysis is based on continuous plant operation with 1% failed fuel - the design basis failed fuel fraction. Following such a postulated release, the automatic containment isolation system, described in Section 6.2 and 7.3 of the PSAR, would be activated and containment isolation effected; the potential consequences of this event would be limited to direct gamma shine exposure from the radioactive cover gas released to containment and to leakage of the cover gas activity through the low leakage RCB.

The design leak rate of the RCB is 0.1% Vol/Day at 10 psig. For the postulated event considered, no mechanism exists to pressurize containment. However, for conservative analysis, a constant 1 psig containment overpressure was assumed. This 1 psig overpressure is a conservative allowance for building heatup, following containment isolation and possible barometric variations. Based on a square root pressure-leakage relationship, containment leakage at 1 psig is 0.032% Vol/Day, or on a fractional basis, 3.7×10^{-9}/sec.

Table Q001.258-1 itemizes the isotopic primary system cover gas inventory, based on continuous plant operation with 1% failed fuel; for this analysis this radioactive inventory is assumed instantly released to the RCB. Column 2 of the table lists the activity per isotope released to the environment during the first 2 hours following the postulated event. Column 3 lists the total activity per isotope released to the environment. These environmental releases were determined considering radioactive decay during holdup in the RCB and continuous leakage from the RCB at 0.032% Vol/Day.

Table Q001.258-2 summarizes the potential site boundary and low population zone doses resulting from this postulated event. As the table indicates, a large margin (greater than a factor of 10^5) exists between the potential doses and the 10CFR100 guideline values. It is therefore concluded, that even for this hypothetical case cover gas release, no danger to the health and safety of the public exists.

Table Q001.258-1

Radioactivity Release Following Hypothetical Cover Gas Release to RCB (Curies)

Isotope	Primary Cover * Gas Inventory	Environmental Release 0-2 Hrs.	Total
Xe131m	26.2	6.89-4 * *	1.16-1
Xe133m	816	5.57-1	8.41-1
Xe133	14,900	3.90-1	35.2
Xe135m	2,340	1.13-2	1.13-2
Xe135	56,900	1.39	9.98
Xe138	3,710	1.98-2	19.98
Kr83m	1,410	2.64-2	5.09-2
Kr85m	3,930	8.88-2	3.29-1
Kr85	0.49	1.29-5	4.61-3
Kr87	3,600	5.83-2	8.89-2
Kr88	6,840	1.42-1	3.64-1
Ar39	35.2	9.28-4	3.33-1
Ar41	27.0	4.99-4	9.39-4
Ne23	1.41+6	2.79-1	2.79-1
H3	8.82-3	2.32-7	8.30-5

* Based on continuous operation with 1% failed fuel. Includes Reactor, Overflow Vessel and PHTS Pumps Cover Gas.

* * 6.89-4 = 6.89×10^{-4}

Amend. 62
Nov. 1981

Table Q001.258-2

Potential Off-Site Doses Following Hypothetical Instantaneous Cover Gas Release to RCB

	Dose (Rem)**	
	Site Boundary (2-hours) (0.42 miles)	Low Population Zone (30-days) (2.5 miles)
Total Whole Body*	6.03×10^{-4}	3.26×10^{-4}
Thyroid	2.17×10^{-11}	6.22×10^{-11}
Lung	1.34×10^{-5}	6.87×10^{-6}
Bone	0	0
10 CFR 100 Whole Body	25	25
Thyroid	300	300

* Includes gamma cloud and inhalation doses.

** Atmospheric dispersion based on 95th percentile χ/Q's per Amendment 38 to Chapter 2 of the PSAR.

Question 001.259 (15.7.x):

Provide analysis of leaks in the EVST NaK system, including the EVST NaK airblast heat exchanger.

Response:

The response to this question has been incorporated into the PSAR by addition of Sections 15.7.1.6 and 15.7.2.6.

Question 001.260 (Appendix D)

Tabulated by page number and paragraph are some very brief notes/comments/ observations on what appear to be statements that either acknowledge existing uncertainties or provide ambiguous judgemental statements about the current CDA analyses which require additional information for support and clarification.

TABLE 001.260

PRELIMINARY SUMMARY COMMENTS ON ACCIDENT ANALYSIS SECTIONS IN APPENDIX D OF CRBRP PSAR

	PSAR Location	Identified Uncertainties
a.	Third paragraph from top P. D2-2	"Clad and FCI relocation mode unrealistic" - Explain and justify
b.	Last paragraph P. D2-3	"Use of SAS beyond verified capabilities" - Explain and expand
c.	Last paragraph P. D5-2	"Ejected fuel distribution and stability" - Explain bases of statement, including scoping calculations
d.	Footnote 2 P. D5-3	Define "maximum consequences" context of a complete analysis
e.	Section D5.1.4 P. D5-6	Describe the application of meter variations to accident scenarios and derived loading
f.	Section D5.4.3 P. D5-7	Insufficient consideration of uncertainties in fuel failure criteria - Give further explanations
g.	Last two paragraphs P. D5-8	Uncertainties in fuel motion - Explain and expand
h.	Section D5.1.4.7 P D5-9	Be more specific on what model uncertainties will be addressed
i.	Next to last paragraph P. D5-10	Need further justification for use of fresh clad properties
j.	Next to last paragraph P. D5-13	Provide the 2D T/H consideration which lead you to conclude the SAS was unrealistic. Also explain how review of FFTF led you to conclude

that hydro-dynamic disassembly for CRBRP give limiting loads. (Does this include recriticality phenomena.)

k.	Last paragraph P. D5-13	"Large uncertainties with clad motion driven disassembly". Explain and justify
l.	Last paragraph Section D5.2.4, P. D5-25	"LOF driven TOP-Fuel Fail Criteria Presently Inadequate". Discuss plans for resolving this important area
m.	Fourth paragraph from top P. D5-29.	Provide more details of bases why fuel motion in SAS is unrealistic
n.	Last paragraph P. D5-29	Justify in more detail omission of SAS/FCI fuel motion

Response:

This question requests clarification of information which is no longer a part of the current documentation. The Project has since consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. A discussion of uncertainties is provided in Sections 6 and 7 of Reference 15, PSAR Section 1.6.

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Question 001.261 (Appendix D)

To illustrate the difficulty we are encountering in our safety review of the CRBRP we offer the following example based on the consideration of clad and fuel motion in CDA analysis. On page D2-2 it is stated that, "...SAS models currently used to predict clad motion in a voided channel and fuel motion following pin failures in a channel prior to boiling provide unrealistically conservative estimates of the reactivity feedback effects due to these phenomena..."

The paragraph goes on to comment that clad motion and FCI models predict overly conservative energy releases which are "not considered meaningful for use in evaluating plant response to the accident." These comments should be extensively justified.

Furthermore, a clearly defined approach to the resolution of uncertainties such as these should be presented. This should include discussions of specific work (both analytical and experimental) that is being, or will be, undertaken to remove the uncertainties that currently exist in the CDA evaluations. The discussion should address how these new results will be used to make the judgement that the assessment is complete enough (no major unresolved ambiguities) to provide a technically firm quantitative evaluation of CDA consequences.

Response:

This question requests clarification of information which is no longer a part of the current documentation. The Project has since consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. A discussion of updated analyses and development programs are provided in References 10a and 15, PSAR Section 1.6.

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Question 001.262 (Appendix D)

All analyses carried out in Appendix D are based upon the SAS code for the initial phase of the accident. In the light that certain modules of that code provide a physically unrealistic description of crucial phases of the accident (e.g., clad and fuel motion), justify the use of this code to predict the course of a LOF or a TOP accident. This justification must address the use of the results of the code calculations to specify the initial conditions for further phases of the accident because the evaluation of the subsequent events depend crucially upon the initial phase calculation offered SAS.

Response:

The bases for the use of the SAS code are provided in Reference 15, PSAR Section 1.6.

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Question 001.263 (Appendix D)

"Engineering judgement" is invoked in the presentation of the CDA analysis given in Appendices B and D of the ER and PSAR respectively to account for gaps in knowledge of the course of events in the accidents. No technical basis is provided for the judgements made to predict the accident scenario and its consequences. Provide the technical and experimental basis upon which engineering judgement was used in the following areas:

a. Clad motion in voided and partially voided subassemblies.
b. Fuel motion in unvoided subassemblies upon fuel pin failure.
c. The configuration of molten and solidified fuel at the end of the initial phase of the accident.
d. The initial conditions and stages for a transition phase or meltdown course fo CRBR.
e. The ruling out of a power excursion which would lead to a very energetic CDA of the CRBR.
f. The final disposition of the core material is in a dispersed, subcritical configuration involving little or no work potential

Response:

The bases for the HCDA analyses are provided in Reference 10a and 15, PSAR Section 1.6.

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Question 001.264 (Page D5-6)(Section D5.1.4)

Explain the procedure used to accomplish the "weighting" of parametric results when significant changes are found in the results. Discuss especially the "substantial evidence" that exists in these cases.

Response:

This question requests clarification of information which is no longer a part of the current documentation. The Project has since consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. The bases for HCDA analyses are provided in References 10a and 15, PSAR Section 1.6. 60

Question 001.265 (Section D5)

Justify the adequacy of the calculation of the lower plenum pressure as a function of time when using PRIMAR when core voiding is in progress.

Response:

The CRBRP Project has consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. The response to this question is now found in Section 3.2.9 of Reference 15, PSAR Section 1.6. 60

Question 001.266 (Pg. D5-18)

Justify the small sodium vapor velocities (<10 ft/sec) obtained in the BOL-LOF analysis described on this page.

Response:

This question requests clarification of information which is no longer a part of the current documentation. The Project has since consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. A discussion of updated analyses and discussions of uncertainties are provided in Section 7.2 of Reference 15, PSAR Section 1.6.

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Question 001.267 (Pg. D9-9)

Using the total fuel (80,000 ft^2) surface area for condensation calculations appear unreasonable when considering accident conditions. Justify the use of this value.

Response:

This question requests clarification of information which is no longer a part of the current documentation. The Project has since consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. A discussion of updated analyses are provided in Reference 15, PSAR Section 1.6.

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Question 001.268 (D5.2.2.2)

Provide an explanation of the apparent inconsistency between using a fuel cracking model in SAS which forces fuel against cladding (see Page D5-3) and the assumption that 50% of the maximum axial fuel expansion reactivity effect can be used in the LOF analysis. Include in your explanation fluence and irradiation effects, in particular, those that might affect relative motion at the cladding-fuel interface.

Response:

The CRBRP Project has consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. The response to this question is now found in Section 2.2 of Reference Q001.268-1. 60 6

Reference:

Q001.268-1: W. R. Bohl, J. E. Cahalan and D. R. Ferguson, "An Analysis of the Unprotected Loss-of-Flow Accident in the Clinch River Breeder Reactor with an End-of-Equilibrium-Cycle Core", ANL/RAS 77-15, May 1977 (Availability: U. S. DOE Technical Information Center). 61

Question 001.269 (Section D5.1.6)

Provide detailed analyses including "calculations based on the model of Reference 4 . . . "which are supposed to show that the potential flow of fission gas from the plenum is of no consequence. Include in your analyses the effect of small ramp rates, the effect of high-burnup cladding degradation, and the effect of high-burnup plenum fission gas. Also include an analysis of a plenum failure prior to a FCI-type failure in the active core region.

Response: * 60

Calculation Based On The Model in Reference 4 (Provided as Reference Q001.269-1)

The results of the calculations which show that the potential flow within the pin of fission gas from the plenum to the original FCI failure site would not be of consequence during the time scale of interest for the FCI transient (approximately 100 msec) are shown in Figure Q001.269-1. The equations shown in Table Q001.269-1 were used to calculate the plenum pressure decay and the mass of plenum fission gas transported to the failure site as a function of time. Table Q001.269-2 gives the parameter values used in the equations and indicates their source. The value for the parameter (P_0), the coolant pressure at the failure site, was held constant and was conservatively selected to be the lowest pressure in the FCI zone obtained from the SAS/FCI calculations.

In the calculation shown in Figure Q001.269-1 the ratio of the effective permeability to the dynamic viscosity was conservatively evaluated at five and ten times the selected best estimate values shown in Table Q001.269-2.

As shown in the figure, the calculations indicate that plenum fission gas is released into the FCI zone at a very slow rate and over a very long timescale (minutes) compared to the timescale during which the FCI zone exists in the channel (hundreds of milliseconds). These low gas release rates from the plenum to the pin failure site are due to both fuel radial thermal expansion and swelling, and to molten fuel which has solidified in the fuel-clad gap in the failure region. 60 The solidified fuel is a porous material through which the fission gas can permeate. The in-pile and out-of-pile test data referred to in Reference 4 show that for a reasonable range of effective fuel pin permeability (an adjustable parameter) the model in Reference 4 correctly represents the transient release of fission gas.

Therefore, fission gas release from the plenum to the original failure site (assuming a secondary failure site does not occur in the upper blanket region) would occur at a slow rate and the amount of gas that would be released into the FCI zone over the timescale that it exists in the channel would not affect the FCI zone contraction and fuel sweepout leading to neutronic shutdown. 31

The response was initially prepared in 1975, using the base EOEC TOP case as it existed at that time. The conclusions presented in Reference 15, PSAR Section 1.6 are the result of the current EOEC TOP base case. 60

*Note that Appendix D has been withdrawn from the PSAR in Amendment 24. The text, upon which the question was based, can now be found in Chapter 6 of Reference 15, PSAR Section 1.6. 60

Plenum Gas Release From A Secondary Failure In The Upper Blanket

Should a second failure location be predicted to occur in the upper blanket region, the pellet clad gap may provide a communication path for release of plenum gas into the fuel-coolant interaction zone. The existence of a pellet clad gap in the blanket region will result in larger gas release rates from a failure in the upper blanket region than would occur from the plenum to a core failure location.

A fission gas induced failure in the upper blanket region will result in additional upward voiding in the channel, adding negative voiding reactivity. The addition of fission gas to the fuel-coolant interaction zone will decrease the fuel-coolant heat transfer (hence generate pressure and decrease the sodium condensation heat transfer coefficient (delaying the FCI zone contraction). However, the additional voiding in the upper portion of the channel and the delay in FCI zone contraction and movement out the top of the channel will not change the overall course of the accident or the termination mode of fuel sweepout and neutronic shutdown in the TOP event for the reasons discussed below.

Fission gas release from an upper blanket failure location cannot occur until the pressure in the fuel-coolant interaction zone decreases to a value less than the plenum fission gas pressure. Figure 6-52 in Ref. 15, PSAR Section 1.6 shows the 60
FCI zone pressure in Channel 10 during the BOEC TOP base case event. Plenum fission gas flow into the FCI zone will not begin until the FCI zone pressure drops below the 16.7 atm. plenum pressure. At that time, approximately 20 msec after the initial clad failure, the net reactivity has decreased to approximately $-0.40, due primarily to negative fuel motion reactivity. Additional voiding which would result from the release of fission gas into the FCI zone would not produce a positive reactivity effect which would lead to a power increase. A delay on the FCI zone contraction and eventual movement out of the top of the channel due to fission gas release will not affect the ultimate course of the accident, i.e., fuel sweepout and neutronic shutdown.

A similar conclusion is found for the EOEC TOP base case event. The release of plenum fission gas from a blanket failure location in Channel 5 could not occur until the FCI zone pressure dropped below the plenum gas pressure. At that time the net reactivity is $0.015 and rapidly decreasing due to negative fuel motion reactivity components from Channels 5 and 8. The mass of fission gas released into the FCI zone will not result in the addition of positive sodium voiding reactivity. The FCI zone containing the fission gas will not expand downward into the active core region because the mass and pressure of the fission gas released into the FCI zone is not sufficient to overcome the hydraulic pressure of the liquid sodium at the lower FCI zone interface. Therefore, instead of reversing the liquid sodium column which fills the channel below the lower FCI zone interface, the released plenum fission gas increases the volume of the FCI zone and causes the upper interface of the FCI zone to expand upward against the hydraulic pressure of the sodium in the upper plenum. The sodium mass above the upper FCI interface (including the upper plenum) exerts a gravity head on the upper FCI zone interface which is much lower than the hydraulic pressure force exerted by the operating pump on the sodium column below the lower FCI zone interface. Therefore, the primary effect of fission gas release into the FCI zones would be a delay in the expulsion of the FCI zones out of the top of the channels, but this would not affect the ultimate course of the accident, i.e., fuel sweepout and neutronic shutdown. 31

Ramp Rate, Cladding Degradation and High-Burnup Effects

In regard to the effect of ramp rate on a secondary failure in the upper blanket region, the SAS TOP analyses in Ref. 15, PSAR Section 1.6 using ramp rates of 2.4¢/sec. to 50¢/sec. did not predict a secondary blanket region failure in the BOEC or EOEC cores. The primary effect of high burnup on cladding is to decrease the cladding failure strength which is described in the discussion of Pointer 50 and by the cladding wastage allowance described in Pointer 52 in Chapter 4 of Ref. 15, PSAR Section 1.6. In order for cladding failure to occur in the plenum region, the effect of higher cladding temperature in the plenum region must be greater than the effect of higher clad loading and greater cladding ductility degradation due to the fluence and burnup effects in the core region. The amount of retained fission gas that is released increases with fuel burnup. However the temperature of the gas and the temperature of the cladding, as well as the mass of fission gas in the plenum, must be considered when evaluating the possibility of a secondary cladding failure in the upper blanket or plenum regions. 60

The combined effects of plenum fission gas loading, cladding transient temperature, pre-transient fluence and cladding temperature, fuel adjacency effects, and cladding wastage on cladding failure are discussed in the response to Question 001.455. Based on these considerations, it is concluded that the possibility of cladding failure in the plenum prior to cladding failure in the core is expected to be very low. A small number of pins could conceivably experience plenum failure, but the coherent failure of large groups of pins in this manner is unlikely.

However, if plenum region failures did occur, the larger mass of plenum gas available in high burnup pins would result in additional voiding in the upper portion of the channel and additional delay in the FCI zone contraction and movement out of the top of the channel, this would still not be expected to result in a large neutronic sodium voiding effect or affect the fuel motion in the FCI zone to the extent that the overall course of the accident, i.e., fuel sweepout and neutronic shutdown, would be changed.

Plenum Failure Prior to an FCI-Type Core Failure

The result of a plenum region failure prior to an FCI type failure in the active core region is discussed in the response to Question 001.455.

Reference

Q001.269-1 GEAP 13923-2, "Sodium Cooled Reactor Safety Engineering Program, Second Quarterly Report, November, 1972 - January, 1973", p. 2-18, February, 1973. 31

Table Q001.269-1

Reference 4 Equations 10 and 11 for
Plenum Pressure Decay and Gas Release Mass

$$p_1(t) = p_0 \left[\frac{1 + \left(\frac{p_{10} - p_0}{p_{10} + p_0}\right) e^{-p_0 ct}}{1 - \left(\frac{p_{10} - p_0}{p_{10} + p_0}\right) e^{-p_0 ct}} \right] \qquad (10)$$

$$\dot{m}(t) = \frac{V}{RT}\dot{p}_1(t) = \frac{2p_0^2 cV}{RT} \left\{ \frac{\left(\frac{p_{10} - p_0}{p_{10} + p_0}\right) e^{-p_0 ct}}{\left[1 - \left(\frac{p_{10} - p_0}{p_{10} + p_0}\right) e^{-p_0 ct}\right]^2} \right\} \qquad (11)$$

Nomenclature

A = Internal cross-sectional area of rod

c = $AKg_c/VL_f\mu$, constant

d_p = Mean particle or channel diameter for the porous flow network

g_c = Conversion factor

L_f = Axial flow path length from plenum to defect

$\dot{m}$ = Gas mass flow rate

m_1 = Fission gas mass in plenum

p_1 = Fission gas plenum pressure

p_{10} = Initial plenum pressure at time of failure

p_0 = Coolant pressure at defect

R = Gas constant

T = Absolute gas temperature

t = Time since failure

V = Plenum volume

v = Superficial velocity of the gas

z = Axial coordinate

K = Effective permeability

ρ = Gas density

μ = Dynamic gas viscosity

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Table Q001.269-2

Parameter Values Used in Porous Flow Fission

Gas Release Model in Reference 4 *

PARAMETER	VALUE - UNITS	SOURCE
P_{10}	2.47 MPa	SAS code output
P_o	0.6 MPa	Lowest pressure in FCI zone from SAS/FCI output
$A = \pi r^2$	$\pi(.254)^2 = 0.203\ cm^2$	Design clad inner radius used
K	5.11 millidarcy	Reference 4
V	22.6 cm^3	SAS input value
L_f	58 cm	SAS code output
R	$8.3x10^7/131=6.34x10^5$ erg/°K-gm	Mol. wt. Xe = 131
T	1100°K	SAS code output
μ	6.67×10^{-4} poise	Value for Xe at above conditions

* Xe is the main consistuent of fission gas (∿85%)

31

Amend. 60
Feb. 1981

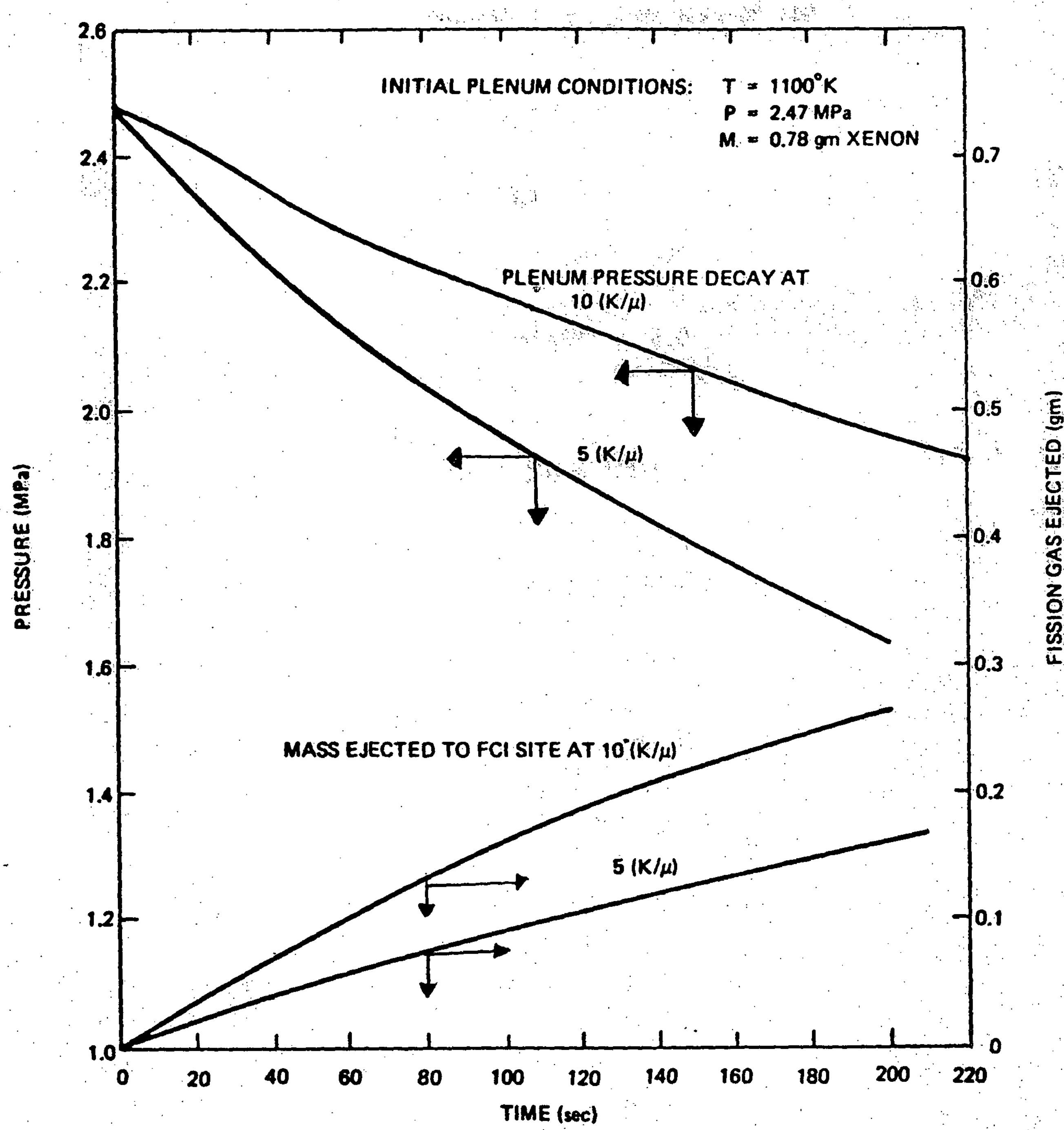

Figure 001.269-1

Plenum Fission Gas Release Versus Time for Appendix D EOEC Top Base Case

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Amend. 31
Nov. 1976

Question 001.270 (ER Appendix B, 1.1.3)

For the TOP HCDA analysis you have choosen a $0.10/second insertion coupled with what you consider "an appropriate nominal case". Consistent with an HCDA analysis the reactivity insertion rate is suppose to be one which, when coupled with the SAS nominal analysis, yields a worst-case accident. Justify your use of $0.10 per second when $0.20 per second is given on Page B-3 as a maximum value, mechanically postulated to occur. Also include in your justification the possibility of relatively-large accidents from much smaller insertion rates. For example, consider a 2¢ per second ramp for the EOEC cycle taking into account high burnup pin plenum failures and subsequent voiding prior to failures in the active core.

Response:

The CRBRP Project has consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. The response to this question is now found in Section 4.3 of Reference 10a, PSAR Section 1.6.

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Question 001.271 (Appendix D)

Item 1.2 of our Feb. 4, 1975 letter addressed fuel failure criteria. We have the following comments on your May 1, 1975 response.

The justification for use of the "burst pressure" failure criterion, especially for low (10¢/sec) reactivity insertion rates, is inadequate. The criterion is associated with one specific failure mechanism -- mechanical loading due to transient fission gas release. It should not be used indiscriminately when other failure mechanisms may be more likely (e.g., for EOEC 10¢/sec ramp where fission gas release at points of local cladding deterioration is likely). Even within its limited scope of applicability, there are modeling weaknesses that must be corrected when using the criterion. For example, failure to properly incorporate cladding ductility for BOL pins is a serious shortcoming that makes application of the criterion to BOL pins questionable. Moreover, all experimental data on fuel pin failures said to support the validity of the burst pressure criterion were under initiating ramps much larger than 10¢/sec. The slower ramps probably emphasize both shortcomings noted above.

The response suggests that SAS does account for "sodium boiling and cladding temperature increase, including melting", but the question of local boiling, e.g., behind wire wrap is not addressed. Also, SAS does not account for fuel swelling due to fission gas precipitation. Yet, the response to the NRC questions says SAS accounts for all "significant mechanisms" for failure except fuel vapor pressure.

The response claims that the 3 FPD burnup does represent initial burnup and restructuring, and justifies this on the basis of experience with other power reactors which see the order of 25 to 50 FPD before full power operation. The implication is that fission gas, not fill or manufacturing gas, produces the cavity pressures needed for pin failure. However, the input data supplied in response to question 6.0 indicate that gas constants more applicable to fill-gas are used for this "fission gas" (unless changes have been made in SSFUEL which were not discussed in the PSAR). The use of inappropriate gas constants introduces order of magnitude errors in calculated cavity pressures for a given gas content.

Also, the C4B test is used to justify a picture of "mechanical failure" for fresh pins. Even ignoring for the moment the significant difference between C4B and CRBRP transient conditions, there are other problems with the use of C4B as a justification for mechanical failure. The response indicates that "sodium bulk boiling" was unlikely (see comments on local boiling above), and that differences in smear densities of C4B and CRBR fuel rods are "reasonably accounted for" in SAS. The latter contention is questionable. Fission gas or fill gas pressure relief via permanent cladding deformation is much easier in the lower smear density CRBR pins. Cladding strain due to the combination of gas pressures and volumetric fuel expansion is simply not properly treated in the SAS code. We certainly recognize the potential for mechanical failure of fresh pins, but such failures must be modeled with full consideration of the mechanical properties of the cladding.

We support the contention that a "perspective" on the importance of failure uncertainties can be obtained by parametric analyses. The studies presented in the PSAR seem inadequate at this point, particularly in terms of failure criteria.

In light of the above comments, please provide further discussion and justification as well as the data and other pertinent information regarding the fuel failure criteria.

Response:

The CRBRP Project has consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. The response to this question is now found in Sections 4.3.2.1 and 4.3.2.2 of Reference 10a, PSAR Section 1.6.

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Question 001.272 (Appendix D)

Item 2.2 of our February 4, 1975 letter addressed an LOF driven TOP type of event. We have the following comments on your May 1, 1975 response.

The contention that the use of SLUMPY rather than SAS-FCI in a partially voided channel is conservative has not been demonstrated. Sodium vapor pressures and vaporization rates predicted by SAS-FCI could strongly influence voiding dynamics and fuel motion (not necessarily in a milder direction).

The suggestion that PLUTO would give less severe fuel motion than SAS-FCI for unvoided channel failures is also unsupported. Comparisons of PLUTO and SAS-FCI referenced in the PSAR are not very applicable to the LOF-driven TOP situations. Coolant momentum, pressures, and temperatures are quite different. Moreover, the differences in fuel motion predicted by PLUTO and SAS-FCI in the referenced article are not even significant in the first twenty or so milliseconds after pin rupture. Possibly, PLUTO would give less positive fuel ramp rates in the LOF-driven TOP case for CRBRP than SAS-FCI, but the suggestion that negative fuel ramp rates would be predicted has no good technical basis. Thus, the implication that excessively high ramp rates were used in the VENUS calculations while disregarding fuel motion is not clearly supported.

In light of the above comments provide a detailed explanation why the SAS/FCI predictions are unrealistic and include the "actual momentum solution for the fuel and sodium motions".

Response:

The CRBRP Project has consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. The response to this question is now found in Section 4.4.3 of Reference 10a, PSAR Section 1.6.

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Question 001.273 (D5.2.5.2 and D5.2.4)

At the end of the analysis of the initial phase of the LOF accident, both BOL and EOEC, a large volume of the core ($\geq$25%) contains sodium. There is a good probability that the power is increasing rapidly in the pins concerned and a substantial amount of reactivity could be inserted upon failure of these pins in the unvoided subassemblies. Provide analysis of this phase of the accident, identifying all major assumptions and their basis.

Response:

The CRBRP Project has consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. The response to this question is now found in Section 4.4.3 of Reference 10a, PSAR Section 1.6.

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Question 001.274 (RSP)

The results of our review of PSAR subsection 3.2-2, "Safety Classifications", are provided in Attachment I. The staff's position on appropriate Safety Classes for the principal CRBRP safety related systems and components is summarized in Table I of Attachment I. Those systems and components for which the staff either disagrees with the proposed PSAR Safety Class, or if it is not clear from the PSAR what Safety Class applies, are indicated in Table I and are specifically addressed.

Provide a complete classification for all the CRBRP safety related systems and components in accordance with the position stated in Attachment I.

Response:

The safety classification of CRBRP piping and components have been modified in accordance with the Regulatory Staff Position above, as interpreted and modified by Reference Q001.274-1.

Tables 3.2-2 and 3.2-5 have been modified to reflect the revised Safety
Classifications. Other pertinent PSAR Sections have been revised to
reflect the new Safety Classes and other design modifications. Note that
49 RAPS has been relocated so that a portion is contained in the RCB and the
remainder located in a RSB cell adjacent to the RCB. The portion of RAPS
outside containment has been designated Safety Class 3. This is based on
49 a modified system configuration which includes two automatic containment
isolation valves at the SC-2 containment penetration.

The Primary Cold Traps are downstream of two automatically operable isolation
valves in the Overflow and Makeup System, and thus need not be Safety Class
1 per Question (RSP) 001.274. Further, as shown in revised PSAR Section
9.3.2.3.1, the cold traps perform no active safety function and may be
isolated from the Overflow and Makeup System at any time. Thus, the cold
traps are specified as Safety Class 3, consistent with the NRC position
on the primary drain, storage and transfer system identified in Reference
49 001.274-1.

Reference:

Q001.274-1 Letter from T. P. Speis to P. S. Van Nort dated March 5, 1976.

Question 001.275 (5.1)

Provide elevation drawings showing principal dimensions (lengths, heights and distances) of the primary and intermediate heat transport systems in relation to the supporting or surrounding concrete structures. Include in the drawings all piping and associated components (i.e., heat exchangers, pumps, check valves, water and steam components, etc.).

Response:

Principal dimensions are given in Section 1.2 and 5.1 of the PSAR. Further details are shown on isometric and layout drawings which will be supplied under separate cover.

Question 001.276 (5.3)

Provide the pump characteristics for the primary heat transport system including: hydraulic torque versus flow at all pump speeds, values for pump inertias, rated hydraulic torques and rated frictional torques.

Response:

Section 5.3.3.3 has been modified in response to this question.

Question 001.277 (5.3)

Table 5.3-15 appears to have an error in the CRBRP column for ultrasonic testing; the acceptance criteria for piping includes flaws $\geq$ one (1) inch long. Please revise this apparent discrepancy.

Response:

Table 5.3-15 contains a typographical error. The sign (greater than or equal to) should read $\leq$ (less than or equal to). Table 5.3-15 has been revised to correct this error.

Question 001.278 (4.2.1.3)

Specify which fuel pin performance code was used to calculate ductility limited strain versus time shown in Figure 4.2-17 (Page 4.2-299).

a) Provide all relevant initial conditions and modeling assumptions used for this calculation.

b) Describe how this strain varies as a function of axial position in the core and power level.

Response:

Revised Section 4.2.1.3.1.1 provides the information requested. 25

Question 001.279 (4.1.2.3)

Specify which fuel pin performance code was used to calculate the effect of transients on strain accumulation, the results of which are given on page 4.2-43. Provide all relevant initial conditions and modeling assumptions used for this calculation.

Response:

Revised Section 4.2.1.3.1.1 provides the information requested. 25

Question 001.280 (4.2.1.3)

Specify which code was used to calculate the cladding hot spot temperatures in Figures 4.2-19 and 20, referred to on Page 4.2-42. Provide all relevant initial conditions and modeling assumptions used for these calculations.

Response:

The curves shown by Figures 4.2-24E to 24J serve as transient envelopes, and as such should not be considered to be calculated information. Various preliminary transient results were studied to help construct these enveloping transient temperature curves. The calculated hot spot cladding temperatures for various undercooling and overpower transients such as those described in Chapter 15.0 are then compared to the pertinent envelope. If the transient has temperatures less than the envelope, the "umbrellaing" process is assumed to be valid. If the transient has temperatures over the envelope, the particular event has to be individually analyzed,
51 and the additional cladding damage included in the rod design evaluation.

The general modeling assumptions and relevant initial conditions for transients can be found in Section 15.2 for reactivity insertion design events and in Section 15.3 for undercooling design events. Also, unique modeling assumptions and initial conditions for particular transients are identified in the "Identification of Causes and Accident Description" portion of each events' description.

The methods utilized to calculate the cladding transient temperatures
51 are discussed in Section 4.4.

Question 001.281 (4.2.1.3)

Figure 4.2-21, referred to on Page 4.2-44, appears to have incorrect values for temperature and an incorrect reference to a figure. Correct these errors.

Response:

Figure 4.2-21 has been amended as indicated.

Question 001.282 (4.2.1.3)

Concerning the HEDL HOP and HUT transient testing program and the HEDL run to failure irradiation program referred to on Page 4.2-52, provide the following information, data and documentation, if any, for all testing to date:

a) all micro and macro photography of pin lengths, sections, and cross sections, including those of all sibling pins;

b) all data and information on fission gas distribution, retention and release including detailed radial distributions for transient tested and sibling pins;

c) all axial and radial strain data;

d) microstructure information;

e) failure locations and assessment of failure mechanisms for all relevant HUT and HOP tests, as well as all run-to-failure steady-state tests and,

f) all neutron radiograph data.

Response:

The HEDL test program was referenced in the PSAR to demonstrate that tests were underway and/or planned to verify the analytical results of Section 4.2. The results of these tests were presented at NRC briefings in November, 1974 July, 1975, and November, 1975. Currently available data and supporting documentation from the HEDL transient testing program and the HEDL run-to-fuel clad breach reirradiation tests are contained in the references listed in the response to NRC Question 001.41.

Although the transient tests performed to date have not been completely prototypic of the CRBR duty cycle events, the accumulated fuel rod transient response data are being utilized to:

a) formulate and verify analytical models for CRBR fuel rod design analysis (for example see the response to NRC Question 241.48);

b) plan future transient tests of prototypic CRBR fuel rods.

All of the data in the HEDL package referenced above are being considered in this manner. More detailed information on specific test data will be made available upon request.

Question 001.283 (4.2.1.3)

Regarding the HEDL test programs referred to on Page 4.2-52, provide a complete inventory on all tests to date indicating the state of the post irradiation and post transient examination. Include dates for anticipated completion of those examinations which are incomplete.

Response:

This information is contained in the references listed in the response to NRC Question 001.41, particularly in HEDL-TME-75-47. This topic was also discussed at the NRC briefing in November, 1975. A description of CRBRP fuel rod transient test activities and the relationship between these activities, the testing program, and the design application of test results will be presented in the response to NRC Question 001.284.

Question 001.284 (4.2.1.3)

Provide a schedule of future HOP and HUT transient tests and discuss how these tests will impact on the CRBRP design.

Response:

Revised Section 4.2.1.4.1 of the PSAR provides the requested information.

Question 001.285 (4.2.3.1.5)

Clarify your statement in Paragraph 2d on page 4.2-163.

Response:

The referenced section has been amended to clarify the statement.

Question 001.286 (4.3.2.1.5)

Provide documentation which describes the referred to experiments at ORNL which:

a) addresses the five SRFM characteristics listed on page 4.3-5a, and

b) verify the IKRD technique described on page 4.3-5c.

Response:

The ORNL experiments referred to have been documented in References 21 and 22 of Section 4.3.

Question 001.287 (4.3.2.3)

Your response to item 001.32 is not complete. Justify using a "T^{-1} temperature dependence" for the Doppler coefficients, in particular for fuel temperatures typical of VENUS disassembly calculations ($T \leq 8,000^{\circ}K$).

Response:

The requested information is provided in revised PSAR Section 4.3.2.3.1.

Question 001.288 (4.3.2.3)

Clarify what is meant by "isothermal temperature conditions", as used in subsection 4.3.2.3.5.

Response:

The response to this question is found in amended Section 4.3.2.3.5.

Question 001.289 (4.3.2.4)

Provide an estimate of the "control rod bite" at the end of the cycle referred to on page 4.3-20.

Response:

The response to this question is provided in amended Section 4.3.2.4, part d.

Question 001.290 (4.3.5)

In Section 4.3.4, you state that, "all of the preceding subsections of 4.3 were based on the use of the LWR discharge grade plutonium.........". Table 4.3-31, referred to on page 4.3-41, appears to have Doppler Coefficients based on the FFTF grade fuel for the first core, and the LWR grade fuel for equilibrium. Explain this discrepancy.

Response:

The response to this question is provided in amended Section 4.3.5.

Question 001.291 (4.3.5.8)

Clarify what is meant by "...consistent calculations...". Indicate if this is to be interpreted that results for the first core with the "FFTF" βeff would give results less severe than the equilibrium core with "LWR" βeff.

Response:

The response to this question is in amended PSAR Section 4.3.5.8.

Question 001.292 (4.3.2.3)

The experimental verification of the CRBRP Doppler coefficient, as presented in 4.3.2.3.1, appears to be based for the most part, on the SEFOR Core II Doppler experiment. Assuming sufficient similarity between the SEFOR Core II and the CRBRP, a test of the adequacy of the calculational capability for the CRBRP would be a calculation of the SEFOR experiment using similar calculational methods and neutron cross section data.

a) It is not clear from Section 4.3.2.3.1, how this test was accomplished, if at all. If reference 12 is the only reference for the analysis of the SEFOR, provide more detail on the differences in methods and cross section data between this reference and the methods and data used for the CRBRP. Indicate if a direct calculation of the Doppler constants for the SEFOR has been performed, and identify the appropriate reference.

b) There were other integral measurements performed on the SEFOR core, e.g., material worth measurements at various core positions. Indicate if attendant analyses of these integral parameters have been performed as checks on the calculational capability.

c) Clarify whether the ± 20% uncertainty in the CRBRP Doppler coefficient applies to the voided core cases. Indicate if the voided-core values are verified by Doppler-effect experiments.

Response:

The response to this question is covered in amended Section 4.3.2.3.1, and added References 4.3-13, 14, and 15.

Question 001.293 (4.3.3.1)

Provide more detail on how the reactivity coefficients were calculated using 2DB and PERT V. In particular, discuss what form of perturbation theory was used, (e.g., exact or first order), what the voided core conditions were, (e.g., was the inter subassembly sodium voided?), and how the leakage contributions were calculated.

Response:

The response to this question is provided in revised PSAR Sections 4.3.3.1, 4.3.2.3.2, and revised Figures 4.3-21, 22, 24, and 25.

Provide the basis for asserting that sufficient flow mixing will take place in the inlet plenum to assure that the inlet temperatures to any fuel, blanket, and control assembly, will not exceed the average reactor temperature by more than 6°F. Indicate how sensitive reactor performance is to the maintenance of this temperature limit, in terms of the development of excessive thermal stresses in the core support structure, modules, and reactor vessel. Verify the adequacy of the references to the figures in subsection 4.4.2.4.1 (e.g., Figures 4.4-5 and 4.4-6).

Response:

The response to this question includes a revision to Section 4.4.2.4.1, revisions to Figures 4.4-4, 4.4-8 (now 4.4-6), 4.4-9 (now 4.4-7) and a renumbering of Figures 4.4-5 through 4.4-10.

Question 001.295 (4.4.2.4.2)

Specify the maximum cladding midwall temperatures in the fuel assemblies and in the radial blanket assemblies on which the orificing scheme has been based.

Response:

The CRBRP fuel and blanket assemblies orificing is discussed and explained in detail in Section 4.4.2.5. Rather than specifying a maximum cladding temperature, the flow in each assembly is orificed to simultaneously satisfy various constraints, such as attainment of lifetime/burnup objectives, satisfaction of transient limitations, and assurance that the assemblies exit temperatures and temperature gradients result in an acceptable thermal environment for the upper internals structure. All the above constraints are quantitatively translated in terms of equivalent limiting temperatures (which are individual characteristics of each assembly) and the flow necessary to satisfy the most restrictive constraint (the lowest equivalent temperature) is determined. Assemblies are grouped togheter in orificing zones (a maximum of eight discriminators in fuel plus inner blanket is allowed) and the total flow allocation to fuel and blanket assemblies must not exceed 94% to account for cooling requirements of other reactor components. Section 4.4.2.5.1 discusses the orificing philosophy, approach and constraints; Section 4.4.2.5.2 presents the method adopted in calculation of the equivalent limiting temperatures, while results are reported in Section 4.4.2.5.3.

Question 001.296 (4.4.2.5)

Identify the specific bases and detailed calculations used in arriving at the reactor pressure drops, as presented in Figure 4.4-2. Identify those results used in establishing reactor pressure drops from the preliminary testing on the Inlet Plenum Feature Model and the applicable FFTF experience referred to in this section.

Response:

The response to this question is given in revised Section 4.4.2.5.

Question 001.297

"Specify the essential code details and model formulations in the Westinghouse proprietary code, FLØPSY, used in the calculation of core hydraulics. What specific aspects of the code make the code proprietary? Describe the extent to which codes, such as FLØPSY and FLØDISC, which are used to calculate code hydraulics, have been verified experimentally. Indicate the extent to which the results from the FFTF Development Programs (based on model tests and applicable to the CRBRP system) are used in order to establish reasonable values of the important resistance and hydraulic characteristics for the reactor internals".

Response:

The FLOPSY code is not Westinghouse proprietary and Appendix A has been revised accordingly. The essential code details and model formulations can be found in reference identified in revised Appendix A and provided under separate cover. The FLOPSY code used standard network analysis techniques, which are discussed in many hydraulic textbooks. While no specific experimental verification of the code has been identified, analytical checks (hand calculations) of flows in various paths have been made.

Similarly to FLØPSY, the FLØDISC code predicts the flow rates in parallel channels composed of a series of hydraulic resistances. The code uses standard form losses and friction factors defined by the basic equations $\Delta P = K \rho V^2/2$ and $\Delta P = (fL/D) \rho V^2/2$. The accuracy of the code in predicting the flow rate and pressure drop in each reactor flow path is, therefore, only limited by the accuracy in predicting form loss and friction factor coefficients. Water flow tests will be conducted to determine the hydraulic characteristics of the fuel, radial blanket and control assemblies over the range of operating conditions. FLØDISC predictions will be used in the design process when experimental data are available on low flow hydraulic behavior.

Finally, regarding the third part of the question, the FFTF Development Programs are not directly applicable to determining exact values for resistance and hydraulic characteristics of the Reactor Internals due to many design differences. However, FFTF experience, data and engineering judgement have been utilized, where applicable, to make predictions for the CRBRP design. Additionally, these hydraulic characteristics have been measured for CRBRP in the Inlet Plenum Flow Model (IPFM) test, and will be measured in the Integral Reactor Flow Model (IRFM) experiment as discussed in PSAR Section 4.4.4.1.

Question 001.298 (4.4.4)

Provide a description of the planned scale model tests, representing the entire reactor, and include an evaluation of the effects of the geometric sca and the effects of the temperature and hydraulic parameters of the test fluid on the results to be used for the full-scale reactor system. Indicate the relationship of these tests to the planned feature model tests. Provide the anticipated schedule of the complete reactor simulation tests and the feature model tests.

Response:

The title of PSAR Section 4.4.4.2 inadvertently implies that Phase II testing in the Integral Reactor Flow Model (IRFM), will utilize a complete reactor hydraulic simulation. The scale of the IRFM was selected to match that of the Inlet Plenum Flow Model (IPFM), to permit combination of the two models into a complete reactor hydraulic simulation, should this be shown to be required. Phase II testing has not been completely defined, and additional work is required to determine if a complete reactor structure simulation is required. Phase II of the IRFM will include an updating of all components to verify the final design for hydraulic and vibrational performance, and if shown necessary, a dynamic core simulation could be included. Sections 4.4.4 and 4.4.4.2 of the PSAR have been amended to incorporate the requested information.

Question 001.299 (5.6.2.9.3.2)

In order for the Overflow Heat Removal System (OHRS) to function properly, the sodium level in the reactor vessel must be high enough to permit overflow into the overflow line and vessel. During a reactor scram, the sodium level will contract and overflow may be interrupted for a period of time. Provide the technical justification to demonstrate that interruption of sodium overflow does not compromise the OHRS safety related function.

Response:

The answer to this question is provided in the revised PSAR Section 9.3.2.2.1.

Question 001.300 (9.1.3)

Since maintenance is intended to be performed on one of the Ex-Vessel Storage Tank (EVST) cooling loops while the other loop is in operation, provide the technical justification to demonstrate that, in the event of a single active failure in the operating loop, sufficient time is available for restoration of one of the cooling loops to service before sodium temperatures reach unacceptable levels.

Response:

The Project is performing a review of the EVST cooling system to determine if the system adequately meets Project requirements. A detailed response to this question will be provided in a future amendment.

Question 001.301 (Chs. 3, 5, 9, 11)

Resolve the numerous inconsistencies in the ASME Code Classes designed for identical systems and components in Chapters 3, 5, 9 and 11 of the PSAR.

Response:

With the exception of the two items listed below, no inconsistencies have been identified in PSAR designations of ASME code classes for the PHTS, IHTS, SGS, SGAHRS, the Nuclear Island Heating, Ventilation, Cooling and Air Conditioning System, Recirculating Gas Cooling System, Chilled Water Systems, or Nuclear Island Treated Water Systems. Two inconsistencies have been resolved as noted below:

a) Table 5.5-7 has been revised to delete the alternative code classification and provide consistency with Table 3.2-5

b) Table 11.2-5 has been revised to indicate "Manufacturers Standards" for pumps per Regulatory Guide 1.26, Rev. 2, June 1975.

As indicated in the response to Question 001.274, the CRBRP Inert Gas Impuring Monitoring, EVST Cooling and Auxiliary Liquid Metal Systems requirements are revised to comply with the NRC position on Safety Classes.

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Question 001.302 (11.1.5)

With regard to the amount of tritium being removed in the primary and intermediate cold traps, provide the experimental basis for the tritium removal efficiency being used.

Response:

The basis for the tritium removal efficiencies is provided in revised PSAR Section 11.1.5.

Question 001.303 (9.1.3)

Provide a discussion on the means to be employed to control the amount of water vapor in the inerted cells containing radioactive sodium equipment. With regard to the water vapor limit of 8,000 ppm specified in Table 3A.1-2, provide the basis that this is an acceptable limit to preclude accelerated corrosion in the event of a sodium leak.

Response:

The water vapor limit given in Table 3A.1-2 (8,000 vppm) was based on the desire under normal steady -state operation that there be no condensation on the RGC unit coils.

The fuel handling cell atmosphere impurity content is to be controlled by the Fuel Handling Cell Atmosphere Purification Unit (FHC-APU) which contains an oxygen-gettering unit and a dryer as described in Section 9.5.4.2.

PSAR Sections 9.5.1.3 and 5.3.2.1.4 have been revised to show that the water vapor control in the inerted cells will be adequate to prevent accelerated corrosion.

Question 001.304 (5.1.8)

Section 5.1.8, "Physical Arrangement," should be expanded to include a discussion of the safety considerations incorporated in the arrangement of systems and components. For valves required to operate during anticipated operational occurrences and postulated accidents, include a discussion of the consideration that has been given to locating valves and their operators such that submergence is precluded in the event of a sodium, NaK or water spills.

Response:

Section 5.1.8 has been modified in response to this question.

Question 001.305 (9.0):

Chapter 9, "Auxiliary Systems", should provide the criteria being used for materials selection for the auxiliary systems and components. The subsections of Chapter 9 should also provide a materials list for the components comprising each auxiliary system. For those auxiliary systems that are constructed of carbon steel and contain either primary coolant or primary coolant cover gas, describe the cleaning and storage procedures to assure that the inside surface is clean when put into service after fabrication.

Response:

The answer to this question is provided in the revised PSAR Sections 9.3, 9.5 and 9.8.

The Nuclear Island and Balance of Plant HVAC Systems and components will be designed to withstand corrosion and, therefore, all duct work will be of galvanized steel construction.

The materials for the Chilled Water Systems, Normal and Emergency Plant Service Water Systems and Non-Sodium Fire Protection System piping and components are based on corrosion and system temperature and pressure considerations. All piping associated with these systems will be of carbon steel construction.

The materials for the Auxiliary Coolant Fluid System, Normal and Emergency Chilled Water Systems, Normal and Emergency Plant Service Water Systems and Non-Sodium Fire Protection System piping and components are based on corrosion and system temperature and pressure considerations. All piping associated with these systems will be of carbon steel construction.

The materials list for all major components of the above mentioned systems is not available at the present itme, and will be presented in the FSAR.

The material selection of the following systems is based upon ANSI B31.1 criteria on pressure and temperature limitations:

a) River Water Service
b) Compressed Air
c) Secondary Services Closed Cooling Water
d) Equipment and Floor Drains

All material for systems a through d will be carbon steel or cast iron.

None of the above systems contain either primary coolant or primary coolant cover gas.

Question 001.306 (11.3.2)

In the event that the RAPS cryogenic distillation column fails to operate, provide a discussion of the alternative operating procedure(s) that will be used for disposition of the long-lived gaseous radioisotopes, Kr-85 and Ar-39.

Response:

The requested information is provided in revised Section 11.3.4. 25

Question 001.307 (9.1.4.3)

Provide the experimental basis that confirms the design heat removal capability of the Ex-Vessel Transfer Machine (EVTM) for both forced and natural air convection conditions.

Response:

The requested information is provided in revised PSAR Section 9.1.4.3.

Question 001.308 (6.2.4)

With regard to the valve types indicated in Table 6.2.5, provide a design which includes automatic actuation for the primary mode for valves in the following lines: the argon exhaust to RAPS, the nitrogen exhaust to CAPS, and the gas sampling line. For this class of lines, one automatic isolation valve inside and one automatic isolation valve outside containment are required in accordance with the design criterion Reactor Coolant Boundary Penetrating Containment.

Response:

Table 6.2-5 has been revised to show automatic actuation of the valves, as indicated in Question 001.308.

Question 001.309(5.3.1, 5.4.1, 5.5.1, 5.6.1, 5.6.2)

There is considerable ambiguity associated with the CRBRP decay heat removal capability following reactor shut down under all plant conditions, including normal operation, anticipated operational occurrences, and postulated accidents. Provide a coherent summary to state explicitly the number of cooling loops (in the PHTS, IHTS, SGS, SGAHRS, OHRS) required to remove both short term and long term plant sensible and reactor decay heat, including consideration of the following:

a) starting from rated power and 2/3 rated power conditions,

b) operation with either pony motor flow or natural circulation on the sodium side,

c) operation with either forced circulation or natural circulation on the water/steam side,

d) loss of offsite A. C. power and loss of both diesel generators.

Response:

The response to this question is provided in the new introductory paragraphs to PSAR Section 5.6.

Loss of offsite A. C. power and both diesel generators is not a design basis event for CRBRP. However, as indicated in Section 5.6, the three loops of the PHTS, IHTS, SGS and SGAHRS can remove residual and decay heat from the reator without A. C. power to pony motors (PHTS or IHTS) recirculation pumps (SGS), motor driven feedwater pumps (SGAHRS) or Protected Air Cooled Condensers (SGAHRS) for several hours following shutdown.

Question 001.310(5.4.1, 5.5.1)

In the event of a sodium fire in an IHTS or SGS cell caused by a spray on the side walls, provide the technical justification that maintaining separate, independent IHTS and SGS loops will not be compromised because of loss of structural integrity of a common side wall. In addition, the capability of the IHTS and SGS cell fire suppression systems to cope with sodium sprays on the cell side walls and ceiling should be provided.

Response:

The present fire protection system provides for catch pans with fire suppression decks in the IHTS cells. The project is currently defining a design basis leak for the IHTS cells. Once this is established, detailed evaluations of the effects of sodium spray on cell side walls and ceilings can be performed. However, based upon existing experimental data and the current design configuration, the question can be addressed as follows. In order to maintain separate, independent IHTS and SGB loops, the integrity of common side walls must be maintained in the event of a sodium spray. Although no protection is provided to prevent sodium spray onto the walls and/or ceiling of a cell, sodium spray onto a common side wall can result in only localized damage to that wall. Tests performed at HEDL and ARD indicate that a sodium/concrete reaction results in the degradation of the concrete strength properties. However, this damage will be restricted to the local spray impingement zones; the overall integrity of the wall would not be adversely affected. The depth of the damage caused by such sprays will be determined based upon tests currently planned in the cell liner development program. If it cannot be shown analytically and in a conservative manner that the sodium spray will not cause a breech in the common side wall, then a design modification will be made to accomodate the sodium spray.

There are several features which could be incorporated into the present design to prevent or mitigate sodium spray damage. Splash shields can be used to prevent sodium impingement onto the walls. The use of a wall covering is also a possibility.

In summary, the effects of sodium spray on common side walls will be evaluated for its propensity to cause a loss of separation of loops. It is anticipated that the results of this analysis and ongoing test programs will show the present design to be adequate. However, a modification to the existing design is clearly feasible and will be adopted if necessary.

Question 001.311 (15.2)

In Chapter 15.2, the measure of the reactor response to "reactivity insertion design events" is the thermal load on the cladding, i.e., the hot-spot cladding temperature. Consider the possibility that pins will fail by mechanical loading of the cladding (primary and secondary loading) before failure by sodium boiling and subsequent cladding melting. Calculate the loadings and resultant strains on the cladding, if any, for the family of reactivity insertion design events considered here. Reevaluate the "conservative" assumptions 2 and 3 in light of these added considerations.

Response:

Reevaluation of these assumptions is provided in revised Section 15.2. 25

Question 001.312 (15.2.1)

Provide detailed (nonproprietary) documentation of the FORE-II code. The summary in Appendix A is not sufficient.

Response:

Modifications made to the original version of FORE-II by Westinghouse are currently being documented. The code changes include the modelling topics of:

- Radii at which Fuel Temperatures are Calculated
- Fuel Sintering
- Determination of Effective Thermal Conductivity Between Two Adjacent Fuel Nodes
- Calculation of the Fuel Centerline Temperature
- Melting of Mixed Oxides Fuels
- Axial Weighting Factors for Doppler Feedback
- Control Rod Scram Options
- Axial Variation in Gap Conductance
- Pressure Drop and Transient Flow Calculations (Flow Redistribution)
- Local Hot Spot Cladding Temperature
- Additional Reactivity Feedback Options
- Normalized Flow Coastdown
- Temperature Dependent Cladding Thermal Conductivity

32 These modifications were in Reference Q001.312-2 provided to NRC in November, 1976. A detailed description of the basic code structure and details of many of the nuclear and thermal hydraulic models can be found in the original code description given by Reference Q001.312-1.

Reference

Q001.312-1 N.J. Fox, B.E.Lawler, and H.R. Batz, "FORE-II, A Computational Program for the Analysis of Steady-State and Transient Reactor Performance", GEAP-5273, September 1966.

Q001.312-2 J.V. Miller, and R.O. Coffield, "FORE-2M: A Modified Version of the FORE-II Computer Program for the Analysis of LMFBR Transients", WARD-D-0142, May, 1976.

32

Question 001.313 (15.2.1.3)

Provide the results of the analysis for the SSE and OBE for the secondary control rods.

Response:

Analysis of a step reactivity insertion postulated to occur as a result of an SSE and terminated by the secondary control rods when tripped by the secondary portion of the Plant Protection System is provided in Section 15.1.4 of the PSAR. This event was selected as the "umbrella event" for analysis reflecting the latest design information. The consequences of the event are shown to be within applicable limits.

The consequences of an OBE terminated by the secondary control rods would be less severe than those for the SSE. Detailed analysis results for the OBE will be provided concurrently with an overall update of PSAR Section 15.2 which will be provided prior to NRC issuance of the Construction Permit.

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Q001.313-3

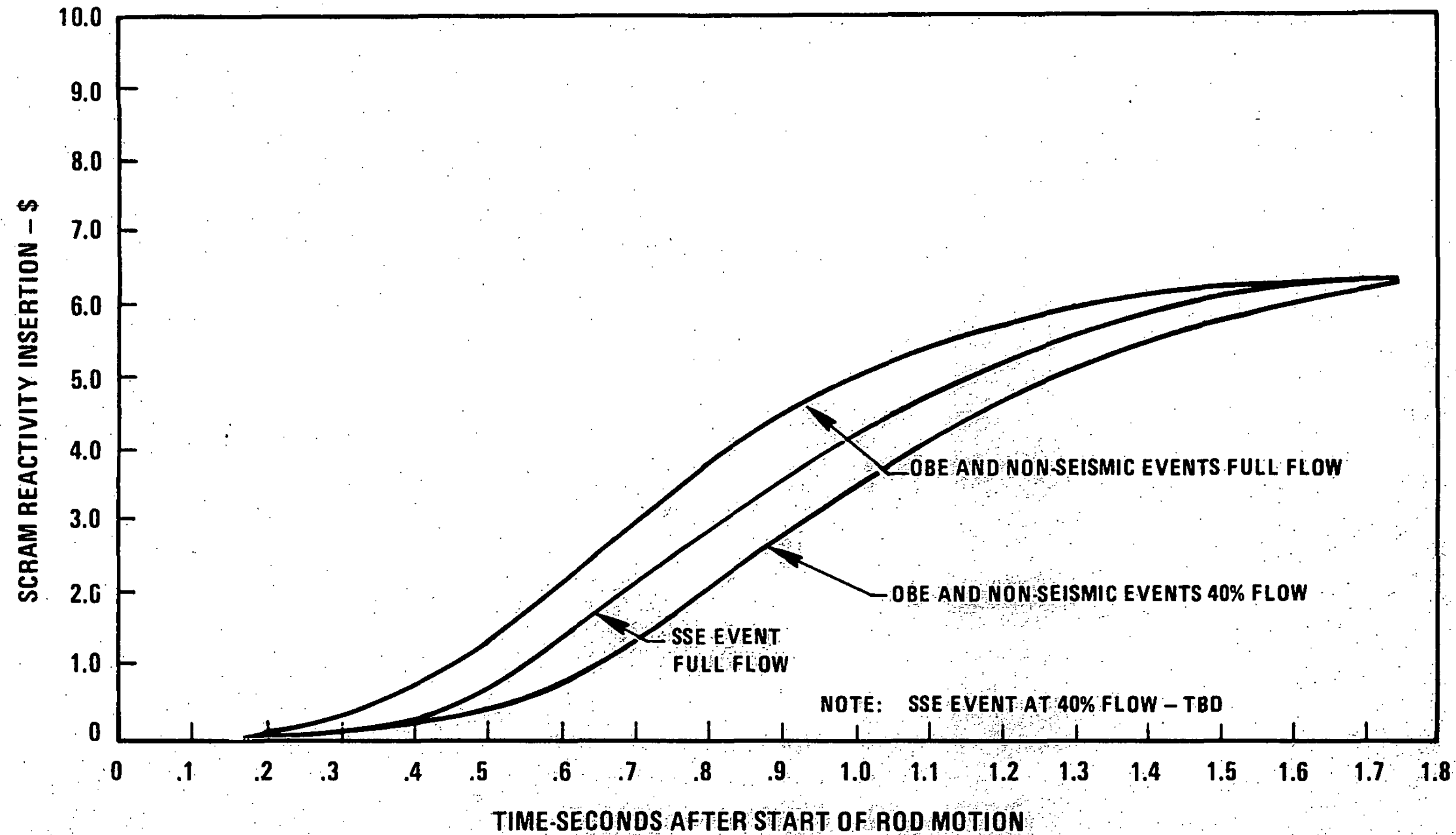


Figure 001.313-1. Secondary Control Rod System Scram Insertion Requirements

Question 001.314 (15.2.1.4, 15.2.2.1, 15.2.3.1, 15.2.3.2)

Further justify your use of the highest power subassembly for BOEC as the "worst" situation for the cases considered. Consider such items as gap closure with burnup and the resultant changes in pellet-cladding heat transfer.

Response:

Revised Sections 15.2.1.4.2, 15.2.2.1.2, 15.2.3.1.2, and 15.2.3.2.2 provides the justification for the use of the highest power subassembly for BOEC as the "worst" situation in the cases considered. 25

Question 001.315

On page 15.2-45, reference is made to Figure 4.2.1.3-4 and Table 4.2.1.3-2. Provide the appropriate information, since this figure and table are not in the PSAR.

Response:

Section 15.2.2.2 of the PSAR has been modified in response to this question.

Question 001.316 (15.2.2.2)

Clarify your conclusion on page 15.2-46 that "no significant degradation of the cladding would be expected...", In particular, consider the mechanical loads and associated accumulation of plastic strain in the cladding for this case.

Response:

The fuel rod analysis results presented in Section 4.2.1.3.1.1 predict no
cladding plastic strain due to steady state and transient thermal loads,
or due to steady state fuel-cladding mechanical contact. Fuel cladding
mechanical loading during reactivity insertion type transients was not
included for lack of pertinent data at the time of writing. Plans to
25 obtain this data have been referenced in the responses to NRC Questions
001.282, 001.283, and 001.284. When available, this data will be
utilized in the appropriate fuel rod performance models which will
be used in preparing the Final Safety Analysis Report.

51

Question 001.317 (15.2.3.2)

The statement on page 15.2-60 regarding "tests as described in Section 15", appears to be in error. Clarify the statement to refer to Section 1.5, or provide a specific and detailed description of the tests to be conducted for the CRBRP design.

Response:

This was a typographical error. The statement should read, "tests as described in Section 1.5". (See revised Section 15.2.3.2.1, page 15.2-60).

Question 001.318 (15.3)

Subsection 1.1.1 of the PSAR indicates that permanent components of the plant have been designed for a stretch power level of 1121 MWt. As indicated in Section 15.3, accidents have been analyzed using the nominal design values corresponding to the design power level of 975 MWt. Although your application is for a 975 MWt power level, it is not clear that the assumed plant parameters used for accident analyses in Section 15.3 are sufficiently conservative. For example, higher primary flow rates may result in higher inlet plenum pressures and increased inlet temperatures which should be considered in your analyses. Provide more detailed discussion regarding your assumption and initial conditions utilized in the accident analyses and justify the conservatism of the selected values recognizing the potential for improved anticipated performance of the permanent components.

Response:

As noted in the question, the permanent components of the plant are designed structurally for a stretch power level of 1121 MWt, though the Chapter 15 transients and the construction permit application are based on a design power level of 975 MWt. The conservatism of stretch condition structural analysis is being applied so that advantage might be taken of more efficient thermal/hydraulic performance which can be optimistically predicted for future operation. Presently, the CRBRP HTS and SGS components are sized and specified so that with a pessimistic combination of pump capacities, pressure drops, and heat transfer characteristics, 975 MWt can be delivered from the nuclear steam supply system.

The low flow,high temperature operating point (with respect to the reactor) that results from this pessimistic set of design values is termed the Thermal Hydraulic Design condition (T&H). It is an extreme operating point of 730^{o}F primary cold leg temperature and 265^{o}F Reactor ΔT. An additional conservative 20^{o}F for control and dead band error was added to the primary cold leg temperature for Chapter 15 analysis.

It is expected that steam generator and IHX heat transfer and primary loop flow will be considerably better than the T&H design values. This would result in a lower primary cold leg temperature and a lower reactor ΔT. Additionally, the expected pump head and primary system resistance curves intersect to produce a slightly lower reactor inlet plenum pressure than for the design case. Since the expected core pressure drop is lower than the design value used for the analysis, this plenum pressure will result in a higher core flow. An estimated expected operating point is then characterized by the following:

Power	-975 MWt
Primary Cold Leg Temperature	-715^{o}F
Reactor ΔT	-T&H

For a more detailed discussion of the meaning of T&H and stretch conditions refer to the response to Question 001.107 (5.3.3.1)

A comparison of the expected conditions and those used for Chapter 15 analysis leads to the conclusion that the analyses were conservative. This has been confirmed by a transient analysis of the loss of off-site power. Except for the variation in initial operating condition, the same set of conservative assumptions discussed in Section 15 were used. As shown in Figure Q001.318-1, the peak clad temperature is 70^{o}F less for expected plant conditions. The conclusion is that T&H plus 20^{o}F conditions are sufficiently conservative for the analysis of plant transients.

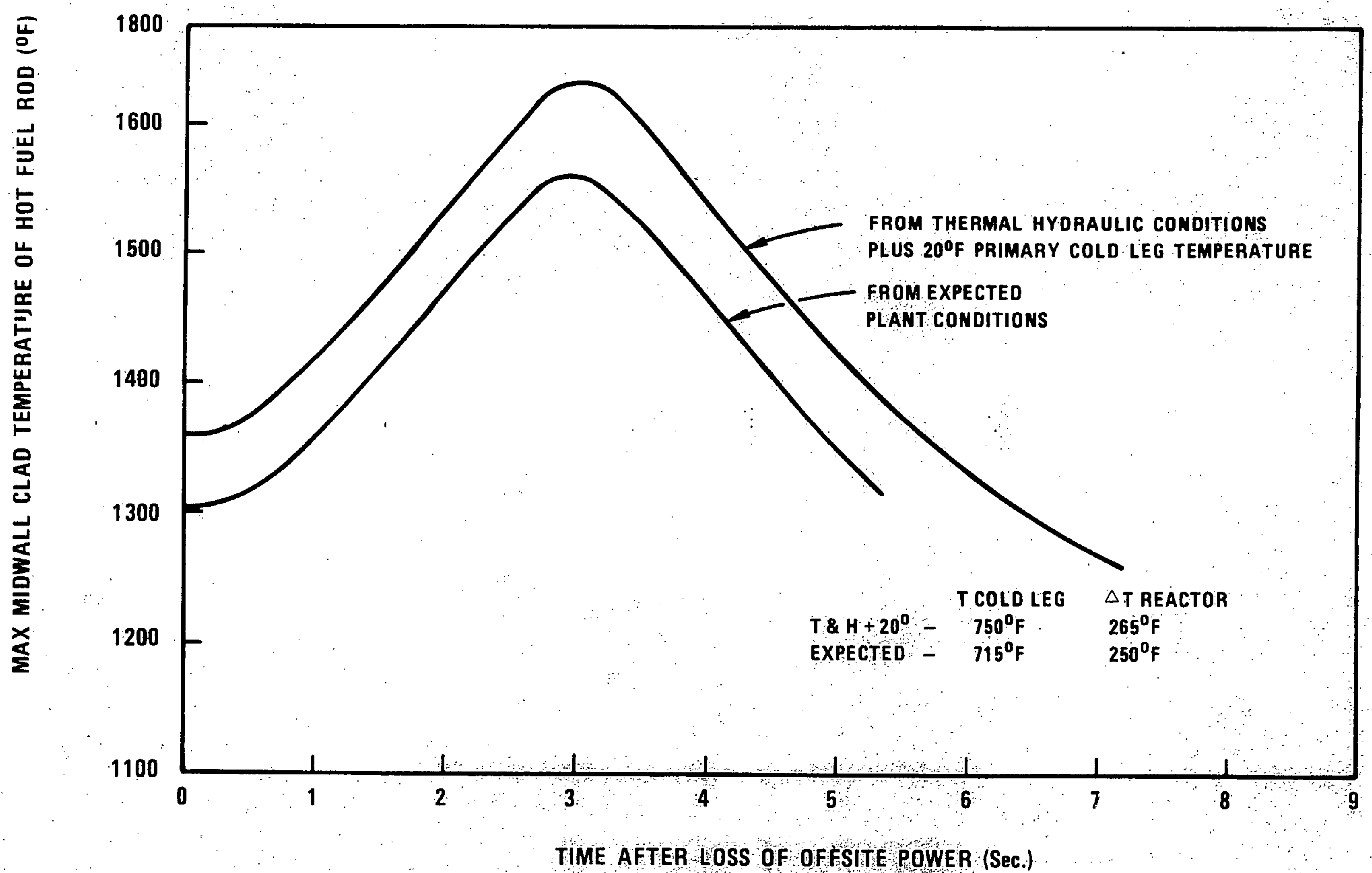


FIGURE Q001.318-1 Clad Temperature Following a Loss of Offsite Power Secondary Trip (Flux/Flow)

Question 001.319 (15.3)

It is suggested in paragraph 6 on page 15.3-3 that the thermal conditions for BOEC represent the worst period in core life. However, mechanical properties for EOEC may be more severe. Discuss what consideration has been given regarding material deterioration for EOEC due to irradiation and thermal creep.

Response:

A discussion of the requested considerations is provided in revised Section 4.2.1.3.1.1. 25

Question 001.320 (15.3.1.2)

In the analysis of a spurious pump trip, no consideration has been given to the sequential loss of flow due to the sequential loss of the remaining pumps. Hydraulic mechanisms such as the propagation of shock waves could result in a sequential loss of flow.

Response:

The requested information is provided in revised Section 15.3.1.2.2 and new figure 15.3.1.2-2. 25

Question 001.321 (15.3.2)

In the analysis of pump seizure, it is stated on page 15.3-29 that there is a possibility of reverse loop flow resulting in a large decrease in core flow. Discuss the possibility of the existence and effects of hydraulic hammer on the operation of the remaining pumps.

Response:

The effect propagated to the remaining loops is discussed in revised Section 15.3.2.1.2.

Question 001.322 (15.3.2)

In the analysis of pump seizure, there is insufficient information to evaluate your analysis.

a. Indicate if the most adverse steady state operating conditions with respect to power level, flow, pressures and temperatures were used.

b. Provide a discussion of the heat transfer coefficients assumed and indicate assigned conservatism in the assumed values.

Response:

Revised Section 15.3.2.1.2 provides the additional information requested. 25

Question 001.323 (15.3.1.1)

For the event of loss of off-site electrical power with scram by the secondary PPS, it is reported (Figure 15.3.1.1-3 and page 15.3-7) that the maximum hot channel temperature peaks at 1630°F. It is claimed that because the temperature exceeds the normal operating temperature for only 6 seconds, the cladding integrity limits are satisfied. However, we note that although the time duration is shorter than the 150 sec for the design basis transient, the peak temperature is approximately 30°F higher than for the umbrella transient event in Section 4.2.1.3.1.1.

The supporting evidence for the cladding integrity under the above conditions seems to be on page 4.2-8 (item 7), "...Transients of short duration and in excess of 1600°F are felt to be within the extra polation capability of the CDF analysis since recent data on irradiated prototypic cladding indicate short-term failure at temperatures much higher than 1600°F.

Provide the evidence in relation to these experiments. Provide the quantitative criteria in terms of Temperature vs. Time for transient events which do not affect cladding integrity.

Specifically address the response to the case of maximum design burnup fuel, taking into account cumulative damage to the clad at the end of a full duty cycle.

Response:

The capability of the CDF analysis to predict the effects of transients of short duration with peak cladding temperatures in excess of 1600°F is fully discussed in Revised Section 15.1.2.1. Revised Section 15.3.1.1.2 provides the additional information requested. 25

Question 001.324 (15.3)

The accidents addressed in Section 15.3 are resulting from singly initiated events. However, some of these events, according to Table 1.2-2 have probabilities of occurrence that are orders of magnitude greater than 10^{-6} per reactor year. This means that the potential exists for multi-initiated events that have a total probability greater than 10^{-6} per reactor year. Provide justification for not including such multi-initiated events in Chapter 15.3.

Response:

These fault events are not combined since the concurrent occurrence of two unrelated events in a way which produces more severe results is too remote to be included in the design basis. The remoteness of the combination of events results because the two events which are unrelated must occur over a very brief time interval to achieve a synergistic effect producing more severe results. The time interval is brief due to the rapid response of the Reactor Shutdown System to the occurrence of the event as described below. (It should be noted that the probabilities in the Table referred to were intended to provide an overview of the event classification, and not to specify the actual probability of occurrence. These probabilities were deleted from the Table in response to Question 001.4 to avoid further confusion.)

Consider the events specified in Chapter 15 of the PSAR. To show the remote nature of synergistic combinations, it is useful to group the events into categories: reactivity insertions; primary flow reductions; intermediate flow reductions; and loss of heat removal through the steam generators (due to steam generator system or balance of plant occurrences). The events analyzed in Chapter 15 include reactivity excursions caused by single rod withdrawal and postulated rod runaway. This latter event umbrellas the results of postulating simultaneous unrelated failure causing two rods to be withdrawn. Similarly, the results of losing the flow in a single loop and in all three loops are presented since there are single postulated events which could cause these sequences. For both reactivity and flow excursions, the transient is terminated by protection system action within seconds. To achieve a synergistic effect, the second unrelated event must occur within a time period of approximately 5 to 10 seconds. Even an event which has a mean time to occurrence of one year only has a probability of occurrence of 3×10^{-7} over a ten second interval. Since the anticipated transients specified are not expected to occur even once per year, this probability of occurrence is a conservative estimate. Further, this probability must be combined with the probability of the first occurrence which further increases the remoteness of the event.

Note that the worst synergistic combination of events involving reactivity insertion and loss of primary flow has been analyzed in Chapter 15 to bound the postulated result of the OBE. For this case, the OBE is postulated to cause loss of power to all three primary pumps followed by a step reactivity insertion at the time of scram initiation due to loss of flow. One of the two shutdown systems (with the additional postulated failure of the most reactive control rod) terminates this event acceptably. This event was analyzed to bound the OBE effects. However, the results show that, even though postulated combined events are too remote to be included in the Design Basis, the results are within the margins of the design.

Combined events involving losses of intermediate flow or heat removal are remote for the same reasons stated above. From the core standpoint, the effect of any anticipated fault in the intermediate or steam generator system is an increase in inlet temperature.

In all cases involving intermediate or steam generator initiating events, the reactor inlet temperature does not begin to increase until after the scram has occurred. Therefore, combinations of these events with each other or with primary flow or reactivity events could not result in synergistic effects on the core temperatures. Similarly, a scram would occur resulting from the flow or reactivity fault prior to any effects of an intermediate or heat removal faults affecting the core. Therefore, though these combined events are too improbable to be included in the Design Basis, the consequences of such postulated events are bounded by the present design basis events.

Finally, the assessments of the Shutdown Heat Removal System do include the probabilities of failures of the various components in determining the probability of successfully removing decay heat.

Summarizing, combinations of two unrelated anticipated transients is too remote for inclusion in the design basis since the time period for initiation of the second event to achieve deleterious synergistic effects is so short. However, other events already analyzed as part of the Design Basis do show that results of postulated combinations would be accommodated by CRBRP.

Question 001.325 (15.4.1.3)

Indicate where the thick (0.4 to 0.8 in.) porous heat-generating blockage considered in subsection 15.4.1.3 (page 15.4-36) is expected to result in cladding failure, relative to the blockage and attendant wake region.

Response:

The information requested is provided in revised Section 15.4.1.3.4. 25

Question 001.326 (15.4.2.2)

Regarding the Consequences of Overpower Pin for Steady State and Design Transients (Page 15.4-47) it appears that only temperature limits were considered and mechanical loads, for example, are not addressed for either steady state or transient conditions. Discuss the transient response of a control rod pin. Include a description of the initial steady-state condition of the pin (e.g., pellet-cladding interaction, if any, as a function of axial position); transient gas release from the B_4C matrix and the attendant loading effects on the cladding; pellet swelling and thermal expansion, and the attendant loading effects on the cladding; pin failure mode and position; and, B_4C particle sweepout or settling, if any.

Response

Additional analyses have been made in response to this question. The results are reflected in revised PSAR Section 15.4.2.2, including new Reference 64 to that section.

Amend. 13
Feb. 1976

Question 001.327 (15.4.1, 15.4.2)

Provide the results of appropriate analyses or experimental verification, if any, to support your statements (pages 15.4-9, 15.4-20 & 15.4-43) that fuel pin failures tend to be self-limiting.

Response:

This question relates to the consequences of gas release from a failed fuel pin. Section 15.4.1.1.3 has been expanded to provide the requested information. 25

Question 001.328 (15.4.1.1)

Regarding Effects of Fuel Particle Release, identify the data and provide the data results which support your statement that "Experience has shown this to be exceedingly low in probability."

Response:

Support for the statement questioned is provided in revised Section 15.4.1.1.5

Question 001.329 (15.4.1.1.1)

It is stated that failed fuel detection systems are being developed. However, the developmental program, its schedule and fallback positions are not provided in Chapter 1.5 of the PSAR. Assuming that failures of a pin-hole variety (with no fuel particle release) occur and that the proposed on-line fuel failure monitoring system development program does not result in the desired quantitative detection system, discuss the impact of this assumption on your proposed operation with failed fuel.

Response:

There are three functions performed by the failed fuel monitoring system: 1) detection of gross cover gas activity change; 2) detection of fuel/sodium contact, and 3) detection of which assembly failed in the presence of existing failures. The development program referred to involves only the third function. As described in revised Section 15.4.1.1.1, an assumption that the development program does not achieve the objectives would involve a potential operational penalty but would not affect the ability to detect failures. 25

Question 001.330 (15.4.1.1)

Provide the appropriate prototypical data and identify the experience cited on page 15.4-13 to support your statement that "rupture size in stochastic cladding failure is likely to be a pinhole."

Response:

The requested information is provided in revised Section 15.4.1.1.5. 25

Question 001.331 (15.4.1.1.6)

Considering the impact of a 1.7% $\Delta D/D$ pellet expansion on irradiated cladding which has lost a considerable fraction of its ductility and assuming initial pellet-cladding contact, discuss the potential for cladding failure.

Response:

It is assumed the question refers to the following statement in Section 15.4.1.1.6, page 15.4-14: "The theoretically predicted uniform linear expansion of CRBR fuel and axial blanket pellets would only be 1.7% and 0.7% $\Delta D/D$, respectively, for extreme reaction conditions after a failure late in life (See Section 4.2.1.1.)."

The subsection in which this statement occurs deals with the long term effects of operation with failed fuel. The prediction made refers to the pellet expansion after the fuel pin has failed. Hence a discussion of "the potential for cladding failure" in irradiated cladding due to this expansion does not appear relevent.

Question 001.332 (15.4.1.2.2)

On page 15.4.18 the statement is made that "test data show that under short term, steady state condition, pin failure is expected only when the molten mass exceeds 25% to 30%." Address the situation assuming long-term conditions and indicate whether there are ongoing tests for this condition.

Response:

Page 15.4-18 has been changed to clarify the position.

The statement was not intended to imply that a fuel melting criterion is appropriate to determine pin failure under all conditions. For example, severe undercooling transients could result in clad overheating and failure without any molten fuel. However, the point being made is that fuel pins can operate even with substantial central melting without failure. The additional information requested can be found in revised Section 15.4.1.2.2.1. 25

Question 001.333 (15.4.1.2.2)

Provide a description of the P-19 and P-20 experiments cited in the PSAR, including complete documentation of the experiments and attendant results.

Response:

The HEDL P-19 experiment was conducted to determine the effect of the fuel/cladding gap size on the linear heat-rating-to-incipient melting for beginning of life conditions. Information was also developed on the fuel characteristics (restructuring, gap conductance) for beginning-of-life conditions. The HEDL P-20 experiment was conducted to evaluate the influence of burnup on the linear heat rating to incipient melting.

The utilization of the data of these experiments in the CRBRP fuel assemblies thermal analysis is discussed in PSAR Chapter 4.4, and in response to questions 241.8, 241.31, 241.32, 241.33, 241.34, 241.35 and 241.40.

Documentation of the experiments and attendant results can be found in:

a) Reference 5 of PSAR Section 4.4, transmitted in response to question 241.37 (P-19)

   Reference 12 of PSAR Section 4.4 (P-19)

   Reference 13 of PSAR Section 4.4, transmitted in response to question 241.37 (P-20).

and the following additional references:

Q001.333-1 R.D. Leggett, el al.,"Influence of Burnup on Heat-Rating-to-Melting for UO_2-PuO_2 Fuel", Trans. Am. Nucl. Soc., 19, pp. 136-137 (1974) (P-20).

Q001.333-2 D.A. Cantley, et.al., "HEDL Steady State Irradiation Testing Program, Status Report through February, 1975", HEDL-TME-75-48, December, 1975, Section V-A. (P-19 and P-20).

Q001.333-3 R. B. Baker, et. al., "Interim Report: Effect of Burnup on Heat-Rating-to-Incipient Fuel Melting-HEDL P-20", HEDL-TME-75-63.

Question 001.334 (15.4.1.2)

Regarding the Thermal Loading of Duct, provide a discussion which considers the possibility, particularly for irradiated and embrittled ducts, that thermal stresses generated by the application of thermal loads will fail the duct before melt-through.

Response:

The requested information is provided in revised section 15.4.1.2.3. 25

Question 001.335 (15.4.1.2)

The conclusion that large cracks are very unlikely (page 15.4-25) appears to be based, in part, on greater duct strength at the inlet. Considering ductility instead of strength, especially for a highly irradiated duct, the conclusion that the crack will be limited to a few inches may not be appropriate. Provide the test results to justify the conclusion for irradiated ducts.

Response:

Test data to firmly establish the ductility of highly irradiated ducts and the variation in ductility with position (due to fluence and temperature variations) are not available. However, these data are not required to arrive at the conclusion that cracking of a duct (even over the full active core length) would have acceptable consequences. The consequences of a postulated large crack were provided in the section.

PSAR Section 15.4.1.2 has been modified to clarify the position. | 20

Question 001.336 (15.4.1.3)

Provide the justification to support your statement on page 15.4-28 that "such particles of debris could only be randomly distributed within the heated zone."

Response:

The justification is provided in revised Section 15.4.1.3. 25

Question 001.337 (15.4.1.3)

Provide justification to support your statement on page 15.4-31 that "if debris deposition occurs it will not occur preferentially."

Response:

The above statement refers to corrosion product deposition in the pin bundle and its potential for blockage formation. As discussed in the response to Question 001.336, deposition within the pin bundle would be random. In addition, a discussion of experimental evidence indicating turbulent mixing in the inlet plenum is provided in revised Section 15.4.1.3.1.

Question 001.338 (15.4.1.3)

On pages 15.4-31 and 15.4-32 it is stated that the core thermocouples will be used to detect blockages postulated to result from debris (corrosion products and lubricating oil) deposition. Operator action will be required subsequent to any warning signals of slow-acting core blockage. The thermocouples are not safety-related instrumentation as defined in subsections 3.2.1 and chapter 7.0. Since this will not be designed as those required for safety, credit should not be taken for any operator action. Assuming failure of the thermocouple system, describe the effects of the above flow blockages on the subassemblies. Include consideration of long-term buildup and the role of PPS related instrumentation to detect and indicate corrective action.

Response:

Although core exit thermocouples might provide an indication of very large blockages, they are not relied upon for the detection of any blockages. Pages 15.4-31 and 15.4-32 have been clarified to reflect this position. The effect of long term buildup of corrosion products is discussed on page 15.4-31. It is noted that corrosion product deposition is not expected in the fuel assembly pin bundle. Even if conservative corrosion product deposition levels are postulated over the entire fuel pin lifetime, the consequences are insignificant.

As mentioned in Section 5.3.2.3.1 of the PSAR, oil leakage into the coolant in the pump tank is improbable, particularly since the leakage reservoir tank which collects any oil leaking past the seals is sized to hold 150% of the total supply oil inventory.

Results of experiments performed at ARD show that even if such a leakage occurs, the reaction products will be small, friable particles which are unlikely to become trapped within the fuel bundle. In these experiments, fifteen microliters (15 µl) of DTE-24 turbine oil was injected onto sodium metal at 850° and 1050°F, the sodium having a surface area of 5.05 cm^2 and a mass of approximately 12 grams. Ten minutes was allowed for possible reaction and the argon cover gas, which was at one atmosphere, was sampled for gaseous reaction products. The gaseous products formed were allowed to equilibrate with the gas sampling system for five minutes and were subsequently identified utilizing established mass spectrometric procedures.

The sodium was allowed to cool to ambient and the stainless steel reaction vessel disassembled. Black particles were observed on the solidified sodium surface in each case. The bulk sodium metal was removed by low temperature (600°F) distillation and the particles collected. The particle residues collected from both the 850°F and 1050°F experiments were similar with the exception of quantity and size. The size and quantity of particulates were greater for the 1050°F reaction temperature. Measurement of particle size was extremely difficult because the friability of the reaction products was such that they disintegrated upon the slightest movement. The largest particles formed were of the order of 40 to 160 mils. However, because of their friability, when exposed to the turbulence of flowing sodium (such as in the primary coolant), such particles would be reduced to sizes small compared to any flow path (on the order of 1 mil or less). They would be easily swept along by the sodium and not become trapped within the fuel pin bundle. If a lubricant leak is postulated, mixing in the inlet plenum would preclude a preferential transport of the reaction product to a given inlet module or assembly. Blockage formation within a given assembly would require an unrealistic quantity of lubricant leakage collection of the small particles in a blockage configuration.

If a large lubricant leakage is postulated through the pump bearing seal, the oil level indicator would indicate the malfunction long before the capacity of the leakage reservoir could be exceeded. However, even if the operator takes no action, no potential for a damaging blockage exists. Even if sequential operator errors and/or malfunctions are assumed and oil is assumed to overflow the leakage reservoir and leak into the sodium (at the estimated leakage rate at which oil passes through the pump bearing seal into the leakage reservoir) over a full month (maximum time between sodium chemical analysis for carbon), a maximum of ~9 pounds of reaction products would be introduced into the primary sodium. This concentration (~10ppm) would not have any potential for blockage formation because of the small particulate size. Even if some deposition is postulated, there would only be small localized reductions in heat transfer which would not increase clad temperatures significantly. The volatile reaction products are very small in volume and would likely be collected in the pump cover gas and removed. However, even if they are postulated to enter the primary loop sodium flow, they would have no impact on primary system component performance. The reactivity and heat transfer effects of such gases as they pass through the reactor core would be negligible. In conclusion, there would be no damage or adverse effects on fuel pin performance, hence no plant protection system response is required.

Question 001.339 (15.4.1.2)

The description of the MARGE/SLUMP CODE in Appendix A is not sufficiently complete. Provide detailed documentation of the modesl used in the code and provide the results of the experimental evidence which support your statement that slumping and formation of fuel bridges will occur.

Response:

The requested detailed documentation of the models used in the MARGE/SLUMP CODE is provided in revised Section A.55. 25

Question 001.340 (15.4.1.2)

The statement on page 15.4-17 that the "fuel-cladding gap will increase and close" appears to be inconsistent and in error. Correct the statement accordingly.

Response:

The statement in question was in error and has been revised.

Question 001.341 (15.4.3.1)

Regarding the mechanical effects of fission gas release on rods and duct walls, it is stated on page 15.4-57 that "the potential for irradiated cladding to burst under this stress needs further investigation." Provide a description, including status and schedule, of your R & D program to resolve this issue. Include in your description an estimate of the associated cladding strain.

Response:

The estimate of stresses described in the referenced section is grossly conservative. It does not allow for load redistribution through adjacent load points as would occur for a breach of the assumed length nor the continuous nature of the rod beyond the assumed contact points. In addition, rod bending, bundle compressibility, rod rigidity due to the internal gas pressure and the dynamic nature of the load are also not accounted for. Furthermore, the consequences of such an additional failure are minimal in as much as the failure would not result in a similar load to additional rods. These consequences are reflected in revised Section 15.4.3.1. Because the consequences of failure of an adjacent rod would be acceptable, no R&D programs have been identified in this specific area.

Question 001.342 (15.4.3.2)

Provide justification, analytical and experimental, if any, to substantiate your postulated failure mode in subsection 15.4.3.2.3 and indicate if consideration has been given to potential failures resulting in secondary loadings.

Response:

PSAR Section 15.4.3.2.3 has been expanded to discuss the basis for postulating the failure mode and reference discussions of secondary loadings.

1!

Question 001.343 (15.4)

Consider a completely blocked, fueled subassembly at full power. Analyze the possibility of subsequent subassembly to subassembly propagation up to and including whole-core involvement. Specify the nature and extent of initial fuel or cladding plugging above and below the core, fuel dispersal and flow regimes and liquid-liquid heat transfer. Include the possibility of failure propagation via the inlet plenum as well as via the S/A duct walls. Perform a similar analysis but with scram occurring. Consider maximum values of decay heat and minimum values of shutdown pony-motor flow in adjacent subassemblies. The analysis should include the transient effects of initial pump costdown and changes in decay heat.

Response:

Fuel assembly blockage as a potential initiator of an HCDA is addressed in Section 3.3.1.4 of CRBRP-3, Volume 1 (Reference 10a of PSAR Section 1.6). It was concluded that large blockages sufficient to cause coolant boiling are highly improbable. Even if an assembly-to-assembly propagation scenario is hypothesized, the consequences would be enveloped by other failure sequences that involve the whole reactor and have been analyzed in detail.

Question 001.344 (15.A)

Define the term "Depletion Factors" used in Table 15.A.3-3 (yellow) and Table 15.A.3-4 (white). In addition, verify the interpretation of the exponential notation used in Table 15.A.3-3 (yellow) which uses two digits to the left of the decimal instead of the usual one digit.

Response:

A definition of the term "Depletion Factors" is provided in revised Sections 15.A.3.2.1 and 15.A.3.3.2 (yellow). With respect to the exponential notation used in Table 15.A.3-3 (yellow), the asterisk note indicating that

$$13.19\text{-}05 = 1.319\text{X}10^{-5}$$

contains a typographical error and should read

$$13.19\text{-}05 = 1.319\text{X}10^{-4}$$

An update PSAR page 15.A-16 (yellow) has been provided to correct this error. 25

Question 001.345 (App. C, 15.0)

Tables 15.1.3-2 and 15.7-1 of Chapter 15 provide the list of all accidents considered in the course of the accident analysis, which result in doses at the site boundary which are within 10CFR100 guidelines. Table 15.7-1 in particular provides a list of "other events". Provide a similar table(s) for Appendix C which contains a list of all events having the potential of exceeding the 10CFR100 guidelines, including all "other faults". In this table(s) identify:

a. The events which are, or will be analyzed within the reliability program (App. C).

b. The events which are discussed or analyzed elsewhere in the PSAR, with a specific cross-reference (subsection and page).

c. The individual events which should be summed up for obtaining the overall probability of exceeding 10CFR100 guidelines.

d. The initial allocation of reliability goals assigned to each one of the relevant events.

Response:

The Reliability Program Plan (Ref. Q001.345-1, Section 1) provides the basis for the scope of the Reliability Program. Based on the rationale provided in that document, only core related events will receive detailed reliability evaluation. These events include failure of the Reactor Shutdown System and failure of the Shutdown Heat Removal System when required, and fuel element failure propagation. Interface failures which could impact operation of either the shutdown or shutdown heat removal systems are included within the appropriate system's reliability evaluation. This includes structural failure of items such as the vessel, core support, piping etc. Other events which have the potential to release radioactive source material, but which are not related to failure of the safety systems, are described in Sections 1.2.2.1 and 1.2.2.2 of the Program Plan, with appropriate reference to the PSAR sections where they are evaluated. Examples of conditions evaluated in this category include refueling machine operation, storage of radioactive material, and natural phenomena such as earthquakes.

The specific events and their associated frequency of occurrence, which are used to determine the yearly unreliability of the Reactor Shutdown System and the Shutdown Heat Removal Systems, are defined in their respective current assessments (References Q001.345-2 and Q001.345-3). The extent to which allocations have been made is described in Section 1.2.3 of the Program Plan.

Thus the specific responses to the four parts of this question are:

26 a. 1. Failure of both shutdown systems when required to act.

2. Failure of decay heat removal system, due to any mechanistic cause, following reactor shutdown.

3. Assembly-to-assembly failure propagation potential.

4. Transients beyond the capability of the Plant Protection System.

b. No core-related event, other than the above, has a potential for producing site boundary doses in excess of 10CFR100.

c. Same as response to a, above.

d. 1. 10^{-7}

2. 8×10^{-7}

3 and 4 combined: 10^{-7}

References:

Q001.345-1. Clinch River Breeder Reactor Plant Reliability Program submitted to the Nuclear Regulatory Commission by the CRBRP Project Office, January, 1976.

Q001.345-2. Reliability Assessment of CRBRP Reactor Shutdown System, WARD-D-0118, Rev. 1, submitted to NRC by CRBRP Project Office, November, 1975.

Q001.345-3. Update of the Preliminary Reliability Prediction for CRBRP Shutdown Heat Removal System, NEDM,14082. Submitted to NRC by CRBRP Project Office, January, 1976.

Question 001.346 (C.2.1.4)

As an integral part of the plant design philosophy the PSAR assumes: (1) all failures are independent; (2) common mode failures (CMF) are highly unlikely due to redundancy, diversity, and equipment physical separation in the design; (3) the project will remove or accommodate CMF via design; and (4) a systematic approach minimizes the likelihood of overlooking potential CMF.

a. Is it correct to state (page C.2.1-21) that the system goals for both the primary and secondary shutdown system have been achieved in the absence of an attempt at quantitative modeling of CMF for the model in Section C.2.1.4?

b. Provide the best analyses completed to date on common mode aspects of the reliability of the specified plant protection systems.

Response:

(a) The analyses presented in Appendix C of the PSAR provide reasonable assurance that important reliability issues relating to CRBRP safety systems will be accommodated and that the Reference Design has capability consistent with stringent reliability goals. Specifically Sections C 2.1.4 and C 2.1.5 of the PSAR present an initial assessment to show that random independent failures do not significantly impact Reactor Shutdown System reliability, and to provide reasonable assurance that this system as designed is capable of meeting its allocated goal even when other significant fault events such as human errors, common mode/common cause failures and test and maintenance activities are considered. Even though no attempt at quantification of any factors other than random independent failures was presented in the PSAR, it is reasonable to assume that the reactor shutdown system will be able to accommodate these other factors within the allocated goal.

System unreliabilities (including common mode failures) of 10^{-4} to 10^{-5} for each of the separate systems (primary and secondary) seem, by the best present practice, to be achievable (see for example Anticipated Transients without Scram for Water Cooled Power Reactors, WASH-1270, September, 1973). Many parts of the CRBRP reactor shutdown system have the same basic design as those used in operating reactors so that the CRBRP shutdown system should be at least this good. The probability of common mode failures can be reduced significantly through design or operational changes resulting from careful application of failure mode and effects analysis and fault tree/event analysis, rigorous testing programs for components and systems, and consideration of human factors in relation to design, testing, operation, and maintenance. This approach is presently undertaken in this program with an exhaustive component level failure mode and effects analysis, a test program which is in place, and the inclusion of human factors considerations.

Even if it is not conclusive that all of these factors can be quantified to th same degree as random independent failures, current studies indicate that they can be sufficiently evaluted so as to judge their potential effect on the goal. Thus it is considered prudent to set such a stringent goal and strive to meet it in such a way that safety system reliability is maximized.

(b) The most recent common mode failure analyses completed can be found in Section 3 and Appendix 9.7 of WARD-D-0118, Rev.1 date November, 1975. It should be recognized that this is an initial effort of an on-going, iterative activity. A brief description of how common mode/common cause failures are considered within the CRBRP reliability program appears in Section 4.2.3 of the Reliability Program Plan and is repeated below to emphasize the importance which the Project places upon resolution of common mode concerns.

The overall approach for resolving common mode failures is similar to approaches for other types of failures; identification and resolution. The failure probabilities associated with the combination of the initiating event and its effect on the system must be statistically credible to warrant resolution. The approach for the CRBRP program is to provide a systematic methodology for (a) identification of potential common mode failure mechanisms, including evaluation of these mechanisms to determine system effects and criticality, and (b) resolution of these failures, and implementation of compensatory features to negate significantly critical effects or reduce them to an acceptable level.

Identification

A search for common mode failures and causes will consider functional dependency, design and manufacturing deficiencies, operational and maintenance procedures, environmental causes, effects of plant accidents, natural phenomena, and others which may evolve from continuous review of LWR operating experiences. This identification task will address the component and system level of the RSS and SHRS, and will utilize the analysis methods described below.

Two different approaches will be made at each level, (a) an event assumption approach, and (b) a fault assumption approach. These will be supplemented by an ongoing literature survey for other identification techniques, a review of operating experience, and a test program which will help to verify the absence of common mode failure mechanisms.

In the event assumption approach, an exhaustive list of potential common mode failure causes and events will be developed and used to challenge the susceptibility of components and systems to failures. To aid in this procedure, a Common Causative Factor Checklist will be developed and sequences of causative factors (chained causative factors) will be considered. This methodology is described in the Reliability Manual Chapter 7, Common Mode Failure Analysis (CMFA). As the hypothetical accident scenarios become complex event tree analysis may be utilized to aid in identifying combinations of events and provide an organized format for systematically dealing with these combinations individually. Event trees are further delineated in Chapter 7 of the Reliability Manual.

In the fault assumption approach, reference components and system will be evaluated to determine modes of failure which may occur, which events may cause failure, whether these can be common mode events, and what the failure effects are. These tasks will be carried out as part of the FMEA process where probability and criticality rankings will be made. A special FMEA form will be utilized for common-mode-failure and criticality analysis, as shown in the Reliability Manual Chapter 7.

Other analysis tasks which may be used for particular systems and components are Fault Tree Analysis or Safety Assurance Diagrams (SAD), (modified event trees).

Resolution

Resolution of identified potential common mode failures will be made in different ways. These include:

- Analysis
- Limit Testing
- Design Changes
- Administrative Controls

Analysis

Potential common mode failures and causes which have been identified will be evaluated to determine their criticality and likelihood of occurrence. If it is determined that the system is safe to acceptable limits without changes, no further actions will be recommended. If the analysis is inconclusive, one of the other approaches to resolution will be utilized. In certain limited cases where common mode initiators cannot be eliminated, probability analysis may be performed to confirm the low acceptable probability of common failures.

Limited Testing

Where practical and when needed, limit testing will be done to confirm safe limits or operating envelopes. Such tests might require environment simulation, statistical sampling, and interface mockup. If tests and analyses cannot confirm system safety, design changes or procedural changes will be recommended.

Design Changes

Design changes to remove the potential for common mode failure mechanism may take many forms. These include:

a. Diversity in concept and detailed design.

b. Diversity in component fabrication sources and techniques.

c. Redundancy of functions, components, and parts.

d. Increased safety margin for added protection.

Administrative Controls

Where it is impractical to make design changes, procedures and administrative controls will be used to assure system safety. Redundancy and diversity may be applied to administrative controls to protect against a procedural error which might lead to common mode failure. Administrative controls are defined including consideration of human factors to reduce the impact of human error as a common causative factor.

Question 001.347 (App. C)

Discuss the reliability-safety analysis of the CRBRP as related to design basis events due to natural phenomena (earthquakes, floods, tornadoes, etc.) within the context of the current reliability criterion.

Response:

The design basis events relevant to the reactor shutdown system are listed in the design duty cycle. Using the design duty cycle as a basis, a reliability duty cycle was formulated bv considering only safety implications of transients rather than any equipment lifetime implications. Also, some of the event descriptions were modified to reflect this safety concern. Those events used in reliability analysis are listed in the reliability duty cycle. These duty cycles are presented in appendix 9.2 of WARD-D-0118, Reliability Assessment of CRBRP Reactor Shutdown System, Rev.1, dated November 10, 1975.

The reliability analysis of core-related radioactivity sources using these duty cycle events, which consider natural phenomena, is presented for the reactor shutdown system in WARD-D-0118, Rev. 1. The influence of transient and accident conditions on component failure rate data is described in the response to PSAR Question #001.349. The analysis of non-core-related radioactivity sources is presented in sections 15.5, 15.6, and 15.7 of the PSAR. Other events which are considered statistically remote are discussed in the CRBRP Reliability Program Plan, transmitted to NRC on January 13, 1976.

A discussion of the design basis events due to natural phenomena and the reliability/safety analysis within the context of the current reliabilitv criterion is presented in the CRBRP Reliability Program Plan. The earthquakes, floods, tornadoes, etc., forming a part of the design bases for the plant, are selected in accordance with past licensing practice, and not in a probabilistic manner. The reliability evaluations will not include assessments of the probability of failures due to natural phenomena (earthquake, floods, tornadoes, etc.) beyond those considered within the design basis.

Question 001.348 (App. C.)

Compare reliability calculations based on natural environmental phenomena to reliability calculations and solutions based purely on all other non-environmental events.

Response:

The reliability treatment of natural phenomena is described in the response to Question 001.347. When assessing a systems reliability, the impact of all potential environmental conditions must be considered. The safety systems are always operating in an "environment" which is changing with time. All events within the design bases of the CRBRP Reactor Shutdown System (RSS) and Shutdown Heat Removal System (SHRS) are considered (with proper frequency of occurrence weighting) when determining reliability. This includes consideration of the impact of natural phenomena on the system as well as normal and abnormal operating conditions created within the Plant. The events considered in the reliability evaluation of the RSS and SHRS are described in their respective current assessment (References Q001.348-1 and Q001.348-2). Calculations have not been performed either strictly including or strictly excluding natural phenomena.

References:

Q001.348-1. Reliability Assessment of CRBRP Reactor Shutdown System, WARD-D-0118, Rev. 1, submitted to NRC by CRBRP Project Office, November 1975.

Q001.348-2. Update of the Preliminary Reliability Prediction, January 1976, for CRBRP Shutdown Heat Removal System, NEDM-14082. Submitted to NRC by CRBRP Project Office, January 1976.

Question 001.349 (App. C.)

Several components will be required to function under both normal operation and under transient or accident conditions. It is not clear whether the reliability analyses will consider component failure data based on these different environments. Discuss how the reliability analyses for each component will take into account the failure data for the postulated conditions associated with those transients and accidents for which the components are required to function.

Response:

Revised Sections C.1.3.4, C.2.1.3 and C.2.2.1 provide the requested information.

Question 001.350 (App. C.)

The following questions are based on Figure C.1-1:

a. When will a complete fault-tree of loss of in-place coolable geometry which considers all relevant initiators be available?

b. Can the transfer-in symbol 20 as it appears on Sheets 2 and 9 of Figure C.1-1 be explained?

c. Provide additional justification as to why random and common mode failures are completely separated in the tree on Sheet 9 of Figure C.1-1.

Response:

a&c. It is not intended to provide a complete fault-tree analysis of loss-of-in-place coolable geometry. Selected fault tree/event tree analyses will be conducted to aid in the systematic identification of common cause events. An example is the detailed evaluation of initiating events associated with fuel element failure propagation.

b. Sheets 2 & 9 of Figure C.1-1 have been corrected and the symbol 20 has been deleted or replaced by the appropriate symbol.

Question 001.351 (App. C. App. B.)

The objective of having less than 10^{-7} SDS probability failure per year that could lead to loss of in-place coolable geometry was addressed by:

1 A goal to provide a design such that the unavailability of the two SDS would be less than 5×10^{-8}.

2 Expected number of times that the protection system is challenged per year is approximately two.

a. Is it possible to construct the probability distribution of the number of times the protection system is challenged during reactor lifetime?

b. What are the frequencies of the individual emergency events considered in Appendix B.

Response:

a. An initial reliability duty cycle has been constructed which provides the frequency distribution of number and type of transients to which the Reactor Shutdown System is exposed. A probability distribution of the number and type of challenges to the protection system was derived for a single worst year (thought to be realistic for early years of operation, later years would have fewer challenges). The design duty cycle was used as a basis for the formulation of the reliability duty cycle. The probability distribution was derived assuming that challenges to the protection system arrive in a random fashion following a Poisson arrival process. Details of the reliability duty cycle can be found in Section 9.2 of WARD-D-0118, Reliability Assessment of CRBRP Reactor Shutdown System, Rev. 1, dated Nov. 10, 1975.

b. The frequencies of the individual emergency events in the duty cycle have not been specified for either structural analysis or reliability analysis. The events specified in the emergency category are considered to be such that while one occurrence of a single event may take place during the plant lifetime, when taken individually, the events are not likely to occur at all. For the structural analysis, the analyst must consider 5 occurrences of the most severe event for each component, plus two consecutive occurrences of the most severe event (or most severe unlike events if the consecutive occurrence of those events is more severe than two consecutive occurrences of the most severe event).

Question 001.352 (C.1, C.2.1)

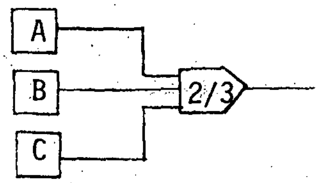
The analysis in Section C.2.1 is based on success-state modeling and block diagrams, i.e., the operational states of the system are found in terms of all possible combinations of operational states of its components and the reliability of the system is computed in terms of its component reliabilities.

a. Compare the success-state method of analysis with a method based on failure probabilities.

b. A fault tree as developed in Section C.1, based on failure probabilities would simplify the analysis and make it easier to follow failure paths. Discuss why this method was not utilized in the fault tree analyses.

Response:

a. A simple two-out-of-three system will be used as an example to compare success-state methodology with a method based on failure probabilities. The system logic is shown below:



Using success-state methodology, a success table must be generated (X indicates a failed state with a failure probability Q while a blank indicates a success state with a success probability R=1-Q).

Success Table

System State #	A	B	C	Sys.
1				
2			X	
3		X		
4		X	X	X
5	X			
6	X		X	X
7	X	X		X
8	X	X	X	X

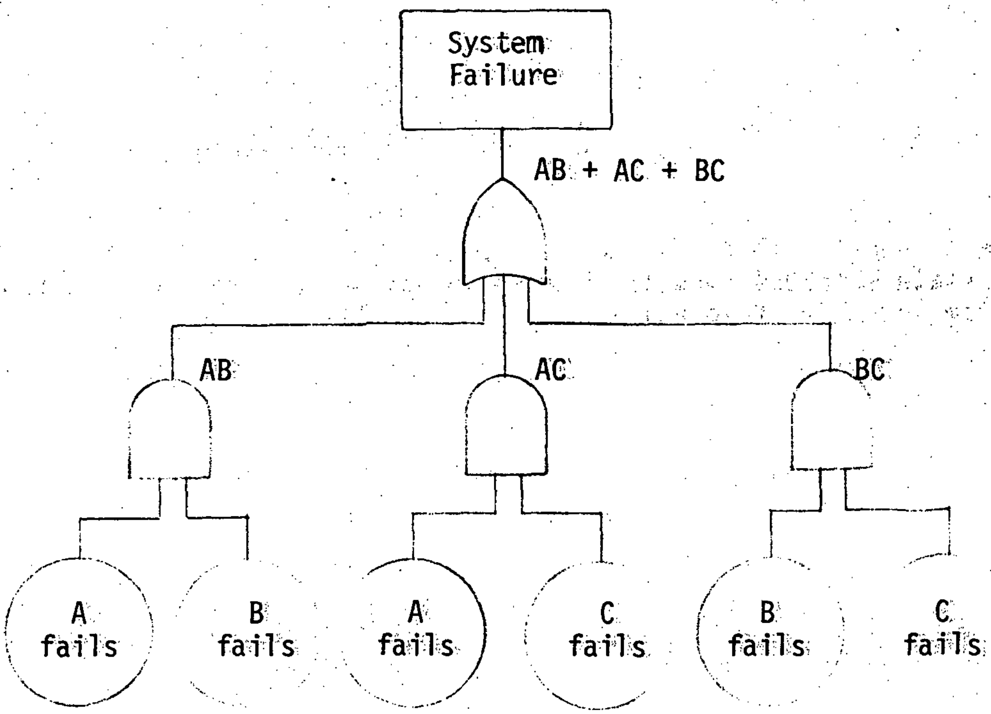
Note that states 1, 2, 3, and 5 lead to system successes. Therefore, the system reliability is:

$$R_{sys} = R_A R_B R_C + R_A R_B Q_C + R_A Q_B R_C + Q_A R_B R_C$$

Setting Q=1-R, $R_{sys} = R_A R_B + R_A R_C - R_A R_B R_C + R_B R_C - R_A R_B R_C$

$$= 3R^2 - 2R^3 \text{ if } R_A = R_B = R_C = R$$

For the same system, a fault tree approach would begin with a system fault tree:



This yields a Boolean solution of:

System Failure = AB + AC + BC

Using Boolean Algebra techniques to find the failure probability expression P,

$$\begin{aligned} P(AB + AC + BC) &= P(AB) + P(AC) + P(BC) \\ &\quad -P(ABAC) - P(ABBC) - P(ACBC) \\ &\quad + P(ABACBC) \\ &= P(A)P(B) + P(A)P(C) + P(B)P(C) \\ &\quad -P(A)P(B)P(C) - P(A)P(B)P(C) - P(A)P(B)P(C) \\ &\quad + P(A)P(B)P(C) \\ &= 3Q^2 - 2Q^3 \text{ if } P(A) = P(B) = P(C) = Q \end{aligned}$$

Now, if the two methods are equivalent,

$$3R^2 - 2R^3 = 1 - (3Q^2 - 2Q^3) = 1 - 3(1-R)^2 + 2(1-R)^3$$
$$= 1 - 3 + 6R - 3R^2 + 2 - 6R + 6R^2 - 2R^3$$
$$= 3R^2 - 2R^3$$

This shows that the two methods compared give equivalent results.

b. The fault tree developed in Section C.1 was developed primarily for qualitative evaluation of loss of coolable geometry. By means of the fault tree, it is easier to follow the failure paths and understand how the system works. However, it is easier to handle monitoring, renewal, maintenance, corrective actions, and duty cycle usage with success state modeling than with fault tree analysis. The PSAR electrical model discussed in Section C.2.1.4 was a first attempt at modeling the monitoring and corrective action aspects of the electrical systems using a Markov model. This model had 6 states which covered all system states which were of interest. The most recent assessment (WARD-D-0118, Reliability Assessment of CRBRP Reactor Shutdown System, Rev. 1, dated November 10, 1975), has an electrical subsystem Markov model (Sections 4.3 and 9.4) of 27 states and takes advantage of monitoring, corrective action and renewal aspects of the electrical subsystems.

The success tables of the success-state modeling allowed a visual expression of the Markov model (page 9-54 of WARD-D-0118). The top level model (Sections 4.2 and 9.1 of WARD-D-0118) also uses Markov techniques to allow mechanical subsystem renewal, duty cycle usage, and modeling of both time and challenge dependent failure mechanisms. This modeling is more easily done with the success state approach than with fault tree analysis. The earlier success-state models allowed a ready transition to the Markov models because the system states were presented and system logic was used directly.

Question 001.353 (C.2.1.5)

In this section there are multiple models for the primary electrical and secondary mechanical systems used in the evaluation of Shutdown System availability. In clarification of the Shutdown System analysis provide the specific equations used in the evaluations of Section C.2.1.5 and relate Equation 2.1.4-11 to Equation 2.1.4-29.

Response:

The results of Section C.2.1.5 are listed below with remarks concerning their meaning or definition.

Case	System	Result	Remarks
(1)	Primary Electrical	$5x10^{-5}$ pg. C.2.1-20	Average unavailability, monitoring, 1/2 safe to total failure ratio, redundant protective function with shared sensor, 720 hour test interval.
(2)	" "	$15x10^{-5}$ pg. C.2.1-21	Average unavailability, 1/2 safe to total failure ratio, redundant protective function with shared sensor, 720 hour test interval no monitoring.
(3)	" "	$14x10^{-5}$ pg. C.2.1-22	Average unavailability, monitoring, 1/2 safe to total failure ratio, single protective function, 720 hour test interval.
(4)	" "	sensitivity Table C.2.1-2	Average unavailability, monitoring, redundant protective function with shared sensor, 720 hour test interval, vary safe to total failure ratio.
(5)	" "	sensitivity Figure C.2.1-15	Average unavailability, monitoring, 1/2 safe to total failure ratio, redundant and single protective functions, vary test interval.
(6)	Secondary Electrical	$6x10^{-5}$ pg. C.2.1-21	Same as No. 1
(7)	" "	$21x10^{-5}$ pg. C.2.1-21	Same as No. 2
(8)	" "	$33x10^{-5}$ pg. C.2.1-22	Same as No. 3

	System	Result	Remarks
(9)	Secondary Electrical	sensitivity Table C.2.1-2	Same as No.4
(10)	" "	sensitivity Figure C.2.1-15	Same as No. 5
(11)	Primary Mechanical	2×10^{-5} pg. C.2.1-20	Rod unavailability of 0.01
(12)	" "	sensitivity Figure C.2.1-16	Vary rod unavailability
(13)	Secondary Mechanical	2×10^{-5} pg. C.2.1-21	Rod unavailability of 0.01, 2/3 logic - 100,000 hr. MTTF
(14)	" "	sensitivity Figure C.2.1-16	Same as No. 12

Average unavailability is found by taking the expression for the system reliability $R_S(\underline{\lambda},t)$, where $\underline{\lambda}$ is a vector of component failure rates and t is time, and integrating this over a time interval of interest then averaging the result, as follows:

$$U_S = 1-A_S = 1-\frac{1}{T_2-T_1}\int_{T_1}^{T_2} R_S(\underline{\lambda},t)dt.$$

Note that all the electrical results are average unavailabilities.

Results number 1, 2, 4, and part of 5 use Equation 2.1.4-29 for $R_S(\underline{\lambda},t)$ which represents the reliability of a redundant protective function with a shared sensor for the primary electrical subsystem. For this case, $\underline{\lambda} = (\lambda_A, \lambda_B, \lambda_C, \lambda_D, \lambda_E, \lambda_F)$ where $\lambda_A = \lambda_{\text{flux sensor}} + \lambda_{\text{transmitter}}$, $\lambda_B = \lambda_{\text{comparator}}$, $\lambda_C = \lambda_{\text{calculational unit}} + \lambda_{\text{comparator}}$, $\lambda_D = \lambda_{2/3\text{ logic}}$, $\lambda_E = \lambda_{\text{summing logic \& logic driver}}$, $\lambda_F = \lambda_{\text{breakers}}$. Below are the data used for each of the above cases. All λ's are in units of 10^{-6} failures/hour.

	Base Data	Case 1	Case 2	Case 4	Case 5A
λ_{FX}	5	2	5	2	2
λ_{TR}	20	4	10	8xK	4
λ_{CM}	12.5	6.25	6.25	12.5xK	6.25
λ_{CU}	16.7	3.33	8.33	6.67xK	3.33
$\lambda_{2/3}$	5	2.5	2.5	5xK	2.5
λ_{SL}	5	2.5	2.5	5xK	2.5
λ_{BR}	12.5	6.25	6.25	12.5xK	6.25
λ_A	25	6	15	2+8xK	6

(Cont'd)	Base Data	Case 1	Case 2	Case 4	Case 5A
λ_B	12.5	6.25	6.25	12.5xK	6.25
λ_C	29.2	9.58	14.58	19.17xK	9.58
λ_D	5	2.5	2.5	5xK	2.5
λ_E	5	2.5	2.5	5xK	2.5
λ_F	12.5	6.25	6.25	12.5xK	6.25
T_1	0	0	0	0	0
T_2	720	720	720	720	T

The base data column represents the input Mean Time to Failures (MTTF's) from Table C.2.1-1. These base data were modified for each of the 14 cases described here. Since no model was developed for monitoring a redundant protective function, a conservative approximation was used. All monitored component failure rates were multiplied by a factor of 2/5. This approximation was developed using equation 2.1.4-11 for a single protective function with K=0.8, R=0.9, and β^{-1}=48 hours based on engineering judgement. (See also response to question 001.355). This can be seen in case 4 where λ_{FX}, λ_{TR}, and λ_{CU} are multiplied by the factor prior to multiplication by a safe-to-total failure ratio factor.

Case 1 is just case 4 with K=1/2, where K is the ratio of unsafe to total failures. Case 4 shows the sensitivity of that ratio. Case 2 does not consider monitoring, therefore no 2/5 factor appears. Note that all sensor failures are unsafe and thus no K factor appears in case 2 or case 4. Case 5 varies T_2 to show test interval sensitivity.

Results numbered 6, 7, 9, and part of 10 use Equation 2.1.4-36 for $R_S(\underline{\lambda},t)$ which represents the reliability of a redundant protective function with a shared sensor for the secondary electrical system. For this case, $\underline{\lambda} = (\lambda_A, \lambda_B, \lambda_C', \lambda_E)$ where $\lambda_A = \lambda_{\text{flux sensor}} + \lambda_{\text{instrumentation}}$, $\lambda_B = \lambda_{\text{comparator}}$, $\lambda_C' = \lambda_{\text{flow sensor}} + \lambda_{\text{instrumentation}} + \lambda_{\text{calculational unit}} + \lambda_{\text{comparator}}$, $\lambda_E = \lambda_{\text{logic signal gear}} + \lambda_{\text{logic train}}$. Below are the data used for each of the above cases.

	Base Data	Case 6	Case 7	Case 9	Case 10A
λ_{FX}	5	2	5	2	2
λ_{FW}	2	0.8	2	0.8	0.8
λ_{IN}	20	4	10	8xK	4
λ_{CU}	16.7	3.33	8.33	6.67xK	3.33
λ_{CM}	16.7	8.33	8.33	16.7xK	8.33
λ_{LSG}	2	1	1	2xK	1
λ_{LT}	8	4	4	8xK	4

(Cont'd)	Base Data	Case 6	Case 7	Case 9	Case 10A
λ_A	25	6	15	2+8xK	6
λ_B	16.7	8.33	8.33	16.7xK	8.33
$\lambda_{C'}$	55.3	16.46	28.67	0.8+31.37xK	16.46
λ_E	10	5	5	10xK	5
T_1	0	0	0	0	0
T_2	720	720	720	720	T

The above data are formulated in the same manner as the primary case.

For results 3 and the rest of 5, the following equation is used to express the reliability of a single protective function in the primary.

$$R_{PE} = \{R_{CH}^3 + 3R_{CH}^2(1-R_{CH})\} \times R_{OL}$$

where R_{OL} is given by Equation 2.1.4-22 and R_{CH} is defined below. $\underline{\lambda}$ is defined as $(\lambda_{CH}, \lambda_L, \lambda_B)$ where $\lambda_{CH} = \lambda_{\text{flux sensor}} + \lambda_{\text{transmitter}} + \lambda_{\text{comparator}}$, $\lambda_L = \lambda_{2/3\ \text{logic}} + \lambda_{\text{summing logic \& logic driver}}$, $\lambda_B = \lambda_{\text{breakers}}$.

	Base Data	Case 3	Case 5B
λ_{FX}	5	2	2
λ_{TR}	20	4	4
λ_{CM}	12.5	6.25	6.25
$\lambda_{2/3}$	5	2.5	2.5
λ_{SL}	5	2.5	2.5
λ_{BR}	12.5	6.25	6.25
λ_{CH}	37.5	12.25	12.25
λ_L	10	5	5
λ_B	12.5	6.25	6.25
T_1	0	0	0
T_2	720	720	T

Case 3 and 5B are monitored and 1/2 safe-to-total failure ratio and similar data manipulation is performed to get to the above table.

For results 8 and the remainder of 10, the following equation is used for the secondary electrical single protective function:

$$R_{SE} = (3K-2)R_{CH}^3 + (3-6K)R_{CH}^2 + 3KR_{CH}$$

where K is $\lambda_{detectable}/\lambda_{CH}$ and $\lambda_{CH} = \lambda_{flux\ sensor} + \lambda_{flow\ sensor} + 2\lambda_{instrumentation} + \lambda_{calculational\ unit} + \lambda_{comparator} + \lambda_{logic\ train}$. This equation is an extension of Equation 2.1.4-11 in order to provide sensitivity information dealing with redundant protective functions versus single protective functions. To find the best that a monitored single protective function can be, it is assumed that all failures of the monitored components are detectable with certainty, and are made safe instantly. This is implied from setting Rm=1 (certain detection), K_{DET}=1 (all failures of monitored components detectable), and β^{-1}=0 hours (instantaneous tripping of channel with detected failure). Note that no monitored factor is used since the equation takes monitoring into account.

	Base Data	Case 8	Case 10B
λ_{FX}	5	5	5
λ_{FW}	2	2	2
λ_{IN}	20	10	10
λ_{CU}	16.7	8.33	8.33
λ_{CM}	16.7	8.33	8.33
λ_{LT}	10	5	5
λ_{CW}	90.4	48.7	48.7
K	63.7/90.4	35.33/48.7	35.33/48.7
T_1	0	0	0
T_2	720	720	T

To demonstrate the conservatism of the 2/5 monitoring factor, using the equation given in the primary case of a single protective function, setting R_{OL}=1.0, and using the following 2/5 factor data (λ_{CH}=27.5x10^{-6}) yields a secondary unavailability of 38x10^{-5} as compared to 33x10^{-5} calculated from case 4. This indicates that the 2/5 factor is a conservative approximation.

For results 11 and 12 use Equation 2.1.4-30 and for results 13 and 14 use Equation 2.1.4-33. Result 13 is derived in detail in the response to Question 001.356, part (b).

Equation 2.1.4-29 applies to primary electrical subsystem redundant protective functions without monitoring. Equation 2.1.4-11 applies to both primary and secondary electrical subsystem single protective functions with monitoring.

An updated model has been developed and used to reassess the shutdown system. The results and a description of the model and data are presented in WARD-D-0118, Reliability Assessment of CRBRP Reactor Shutdown System, Rev. 1, dated November 10, 1975. It is suggested that future questions on the assessment techniques and results refer to this document.

Question 001.354 (C.2.1.5)

In this section it is stated that the quantitative results are based on failure data from Section C.2.1.3. Although some data is available in Table C.2.1-1, Section C.2.1.3 contains only references to data, such as references to FFTF and commercial reactor experience. Provide a systematic, documented listing of the data used to obtain the results of the evaluation of Shutdown System availability (Section C.2.1.5); include a clear identification of the model parameters to which the data relates.

Response:

See response to question 001.353. Also note that an improved model, data, and new assessment results are presented in WARD-D-0118, Reliability Assessment of CRBRP Reactor Shutdown System, Rev. 1, dated November 10, 1975.

Question 001.355 (C.2.1)

A Markov model of the Protective Function Network is used in the evaluations in Appendix C.

a. Indicate how the fraction of detectable failure, K_{DET}, is determined.

b. Provide the numerical value of K_{DET}.

c. Provide the value used for R_M (probability that a detectable failure is detected).

d. Provide supporting evidence for the valuves used for R_M and K_{DET}.

e. Indicate how the four blocks of Figure 2.1.5 relate to the three blocks for the Protective Function Network in Figure 2.1.7.

f. Indicate how β (Page C.2.1-10) is actually determined and what value is used in the calculations.

g. Based on the available data in Section C.2.1, an analysis of Equation 2.1.4-11 indicates that R_M K_{DET} must be approximately 0.99 in order to obtain a Primary Electrical System unavailability on the order of 10^{-5}. This requires both R_M and K_{DET} be greater than 0.99, which is unlikely. Explain in detail how Equation 2.1.4-11 is utilized in the assessment of the Primary Electrical System unavailability. Provide supporting documentation for the data base used in this analysis.

Response:

a. In later assessments, K_{DET} will be determined from the FMEA done on the various electronic components. In the prediction in this Section as in the most recent assessment (WARD-D-0118, Reliability Assessment of CRBRP Reactor Shutdown System, Rev. 1, dated November 10, 1975), K_{DET} was determined by engineering judgement (see revised Section C.2.1.5).

b, c, d, and f. This information is in revised Section C.2.1.5.

e. This information can be found in revised Section C.2.1.4.3.1.

g. Equation 2.1.4-11 is used to compute the reliability of only a single protective function which is monitored. Thus, the equation would model only a part of the Primary Electrical System because the logic network and scram breakers are not part of the protective function network as discussed in Section C.2.1.4.3.1. The values of electrical system unavailability on the order of 10^{-5} are obtained under the assumption of redundant protective functions as shown in Section C.2.1.5.4. However, the most current assessment (WARD-D-0118, Rev. 1, dated November 10, 1975) shows that with a new model and improved estimates of component failure rates, a single protective function that is monitored can lead to unavailabilities on the order of 10^{-5}. Engineering judgement was used for K, R, and β. The Data Bank for the λ's is documented in Table C2.1-1 of the PSAR. The current assessment has a more detailed documentation of data.

Question 001.356 (C.2.1)

This question number was assigned to a continuation of Question 001.355. The information requested has been provided in the response to Question 001.355.

Question 001.357 (C.2.1.5.6)

The expression for the reliability of the Primary Mechanical System of the Shutdown System under the assumption of independent control rod failures is given to Equation C.2.1.4-30. Although it is correct that a rod unavailability of 0.01 implies a system inavailability of $\approx 2 \times 10^{-5}$, the system unavailability is rather sensitive to the rod unavailability in the range of interest. For example, if the rod unavailability is 0.02 instead of 0.01 then the system unavailability increases by an order of magnitude. If the rod unavailability is 0.05, then the system unavailability is $\approx 2 \times 10^{-3}$..

a. Provide further information on the applicability of the data (Section C.2.1.5.6) that supports a rod unavailability of 0.01.

b. The analysis of the Secondary Mechanical System is similarly based on independent rod failures. Based on the availability expression for three out of four rod insertions on Page C.2.1-18 and a rod unavailability of 0.01, the Secondary Mechanical System unavailability is calculated to be $5.9 \times (10)^{-4}$, which is larger than the result of 2×10^{-5} given on page C.2.1-21. Provide the step-by-step analysis, including assumptions and computations, that yields a Secondary Mechanical System unavailability of 2×10^{-5}.

Response:

Information regarding parts a and b of the question is provided in revised Sections C.2.1.5.6 and C.2.1.4.4, respectively. 25

Question 001.358 (C.2.2)

The preliminary model used component and subsystem failure rates to estimate the probability of simultaneous failure of the four redundant heat removal paths. These failure rates are given in Table C.2.2-1.

a) Provide the source of these numerical failure rates.

b) Discuss the plans to improve these numbers if they are currently estimates, based on insufficient data, or are for equipment similar but not identical to that proposed for the CRBRP.

Response:

a) The source of each failure rate is provided as an appendix to the "Update of the Preliminary Reliability Prediction for the CRBRP Shutdown Heat Removal System" (NEDM-14082, January, 1976) which was submitted to NRC in January, 1976.

b) Plans to improve the basis for the failure rates include the following:

i. Continued review of data which become available and continued interaction with appropriate equipment specialists.

ii. Testing of selected components within the reliability program as described in Section C.3.2.2 of the PSAR.

iii. Continuance of the current policy of close follow of the pertinent engineering development and technology testing planned or in progress within the CRBRP Project and the broader LMFBR Program.

Question 001.359 (App. E)

In the initial presentation of this Appendix it is stated that "a reliability assessment of the primary piping integrity, utilizing the PHTS interior stress analysis results will be made by June 1975." In Amendment 5, October 1975, it is stated "an updated reliability assessment of the primary piping integrity, ... will be made by July 1976." Provide this initial reliability assessment of the primary piping, including methodology, procedure, failure data, and actual calculations.

Response

The information in question was submitted to NRC in the "CRBRP Primary Pipe Integrity Status Report" on December 19, 1975. The reliability assessment is contained in Section 6 as supported by Sections 3 and 4.

Question 001.360 (Appendix E)

A basic project position is that large pipe ruptures in the primary heat transport loops have a sufficiently low probability (10^{-8}/year) that they may be excluded as design basis events for the CRBRP. Although the reader is referred to Sections 1.1, 1.5, 5.3, and App. C. of the PSAR, sufficient material to verify this reliability assessment is not provided. Further material in the Preliminary Reliability Assessment, Figure E.4-1, due in June 1975, has not been provided.

Provide sufficient information to verify, in a preliminary manner, the exclusion of a large pipe rupture as a design basis event.

Response:

The requested information has been provided in the "CRBRP Primary Pipe Integrity Status Report", submitted to NRC on December 19, 1975.

Question 001.361 (5.3.3.6.2)

A possible failure mode discussed in this section is piping crack growth under cyclical conditions, in which a crack is assumed to propagate from an initially undetected flaw.

a) Provide a probability density function as a function of size for undetected flaws (or the methods to be used in developing this probability density function). This analysis should consider the current limits of detectability of flaws in stainless steel, particularly when sodium is in the system.

b) Show how the probability density function is used to verify that primary piping failure has a probability no larger 10^{-8}/year, with sufficient details for an independent analysis.

Response:

a) The response to this question is found in Section 3.2 of the Primary Pipe Integrity Status Report (WARD-D-0127 of Dec. 1975). The analysis for an initial undetected flaw does not require consideration of sodium in the system, since no sodium is present during pre-service inspection.

b) The response to this question is found in Section 4.5 and Section 6 of the Primary Pipe Integrity Status Report. A supporting Section is 3.2.

Question 001.362 (5.3.3.6.2)

In this section an analysis shows that an undetected axial crack (1.5 in. long and 0.125 in. deep) due to high circumferential stresses in the cold leg pipe elbow of the inlet downcomer adjacent to the reactor inlet nozzle, a region of relatively severe stress from a fatigue standpoint, exhibits a negligible growth of 6×10^{-7} in. It is assumed that the material is 304 stainless steel at 800°F and that there are 600 significant transient (cyclical) events during the plant service life.

a) Does experimental evidence exist or will it be obtained to support this significant analytical prediction of crack growth? Provide details.

b) What is the effect on crack growth if multiple flaws (undetected) are in close proximity, i.e., does the above analysis assume the single crack is contained within a relatively large area of undamaged pipe?

Response:

This information may be found in revised Section 5.3.3.6.2. 25

Question 001.363 (5.3.3.6.2)

This question number was assigned to a continuation of Question 001.362. The information requested has been provided in the response to Question 001.362.

Question 001 364 (5.3.3.6.2)

A possible piping failure mode which involves a through-the-wall crack in a pipe elbow is shown to have a critical axial crack size (size for which bulging occurs at operating stresses) of 15.4 inches for design pressure and 18.1 inches for operating pressure.

Provide the analysis used to derive critical axial crack sizes, and show the relevance of this failure mode to the initial reliability assessment of 10^{-8} pipe rupture failures/year.

Response:

Undated analyses based on actual experimental results from model elbows are presented in Section 4.6.2, "Piping Critical Crack Sizes," of the Primary Pipe Integrity Status Report (WARD-D-0127, dated December, 1975). Critical crack lengths are given on page 4.6-5. Based on test data with specimens of more nearly prototypic geometry, the critical crack length is revised from 15.4 inches to 20 inches.

In Section 4.6.3.3, defects in an elbow are shown to penetrate the wall before reaching critical crack length. The report, therefore, presents:

1) experimental indication that a flaw will propagate through the wall rather than preferentially extending to critical crack length without wall penetration (and leakage); and

2) the prediction of a very low probability of wall pentration, a failure event much more likely than flaw growth to critical length.

Based on these results, it can be concluded that rupture due to crack growth to critical length is well within the allocated goal of 10^{-8} per reactor year.

Beyond the quantitiative material presented, important considerations which are not yet quantifiable provide strong support for the conclusions of adequate primary piping integrity. System design and engineering have provided a set of duty cycle events in the piping specifications which conservatively represent expected plant lifetime loads. In addition, provision is made for inservice inspection and continuous monitoring. Any leak occurrence will be detected in a timely manner by either sodium leak detectors or radiation monitors. These items, coupled with stringent specification requirements for manufacturing quality and inspection verified by a rigorous quality assurance effort, provide confidence that pipe rupture has an adequately low likelihood of occurrence.

Also of interest is the fact that testing to enable a degree of quantification of the leak before break sequence of events is ongoing. Testing to measure leakage rate versus through-wall crack size is underway. Testing to measure leak detection system capability is likewise underway with data to measure system reliability in more nearly prototypic arrangements planned to be initiated in 1977.

Question 001.365 (5.1.5.2)

Discuss the transient behavior of the DHRS system and what the maximum temperature of the fuel cladding will be when it is in use; under the most severe conditions anticipated. 26

Response:

The transient behavior of the DHRS is in expanded Section 5.6.2.3.9. 26

Question 001.366 (5.2.4.4)

Discuss the buffered seal concept in more detail. Explain the arrangement of seals and how uniform buffer gas down flow is assured, and why channeling on one side cannot occur, which would defeat the objective of the buffered seal, and allow higher concentrations of radioactivity to escape past the buffered seals.

Response

The question as stated refers to a set-down ledge combined with a down purge seal concept where there is buffer gas orificing at the set-down ledge. The CRBRP seal design utilizes a Na dip seal in the 400°F region of the closure head and an elastomer seal in the 125°F region of the riser assembly. There is no buffer gas flow in the annular space. The channeling problem referred to in the above question is not encountered.

Additional description of the sealing arrangement requested is provided in revised Section 5.2.4.4 and new Figures 5.2-7 and 5.2-8.

Q001.367 (5.3.1.1)

Discuss why the requirements for the Heat Transport system are stated as performance objectives. Discuss if there is any implication that if these objectives cannot be achieved, they will not be required, and if so, in what areas are these performance objectives likely to be waived.

Response:

Section 5.3.1.1 is based on 5.2.1.1 Performance Objectives, of the Standard Format & Content of Safety Analysis Reports for Nuclear Power Plants, LMFBR Edition, prepared by Regulatory Staff, USAEC 1974.

There is no intended implication that these objectives will not be met, and to avoid confusion, Section 5.3.1.1 has been retitled, "Performance Requirements". Consistent with this change, specification of two thirds as the rated power for two loop operation has been deleted because it is unknown at what power two loop operation will be most acceptable.

Question 001.368 (5.3.1.1)

Discuss the design basis of the IHX for sodium water reactions and check valve responses. What combinations of amplitude, duration, and number of cycles are used in the design of the IHX for sodium water reaction impulse and check valve closure? Will these be treated as ASME faulted conditions?

Response:

The analytical methods and criteria for demonstrating IHX accommodation of the design basis sodium water reaction and check valve slam are discussed in revised Section 5.3.3.1.5.

Question 001.369 (5.3.1.4.3)

Describe and discuss your proposed gas pressure test for the reactor coolant system, and its objective.

Response:

Section 5.3.1.4.3 has been revised to more clearly describe the gas pressure test entire strength and tightness requirements to be placed on the HTS.

Question 001.370 (5.3.1.5)

Describe and discuss the sodium gas leak detection system, its sensitivity and response as a function of sodium leak rate, for the most severe conditions anticipated, in a low oxygen and moisture environment.

Response:

The answer to this question can be found in Section 7.5.5 of the PSAR.

Question 001.371 (5.3.2.3.4)

Discuss the applicable experience with constant load pipe hangers in areas such as the CRBRP reactor cavity environment for the life expectancy of CRBR. Describe the features being provided for remote maintenance, if any, for inaccessible pipe hangers.

Response:

The concerns expressed in this question are addressed in revised Section 5.3.2.3.4.

Question 001.372 (5.3.3.1)

Provide supplemental information on the methods employed in the design of heat transport system components. How are the nominal values determined, the expected deviation established, and how are these factors combined to determine the most probable expectation of performance? What kind of sensitivity studies are employed for structural design with variations in the sequence of application of loads and variability of materials properties?

Response

The answer to the first part of this question can be found in the description of the Monte Carlo technique for randomly selecting heat transfer coefficients, process variables, and uncertainty ranges as presented in the answer to PSAR Question 001.107.

Revised Section 5.3.3.1.2 provides the information requested in the last sentence of the question.

Question 001.373 (5.3.3.1)

Discuss what tests are planned for the check valves and will these include convective flow tests to establish low flow characteristics as well as full flow tests with sodium.

Response:

Revised Section 5.3.3.1.7 provides the information requested.

Question 001.374 (5.3.3.6)

The discussion presented does not mention the effect of residual stresses from work-hardening, forming, machining, and grinding. Experience with LWRs have indicated that these effects may play an important role in crack initiation. How will these considerations be included in the fracture mechanics assessments being made?

Response:

The CRBRP Primary Pipe Integrity Status Report, submitted to NRC on December 18, 1975, gives detailed information concerning the influence of cold working on fatigue-crack growth behavior for 304 stainless steel (see Section 4.1.4)

Question 001.375 (5.4.1.2)

Is the Liquid Metal Fast Breeder Reactor Materials Handbook to be considered as a Topical Report and if so when will it be submitted in final form for the CRBR project?

Response:

The LMFBR Materials Handbook was the predecessor of the Nuclear Systems Materials Handbook (NSMH). A reference to the LMFBR Materials Handbook is the equivalent to a reference to the NSMH, because the initial publication of the NSMH contained the same data as the LMFBR Materials Handbook. Any modifications to the initial publication are fully justified in Volume II of the NSMH.

The NRC is on distribution for the NSMH and its revisions.

The NSMH where utilized in the PSAR, is referenced in the appropriate PSAR Sections. 42

Question 001.376 (5.5.2.6)

Discuss the basis and experience for materials selection and sizing of the SWRPRS system.

Response:

The information requested is provided in revised section 5.5.2.6 of the PSAR.

Question 001.377 (5.5.2.7)

Discuss what disposition will be made of the sodium reaction products in the event of a steam generator tube rupture. Discuss how the remainder of the intermediate coolant system will be protected against caustic stress corrosion following a steam generator failure.

Response:

Revised Section 5.5.2.7 provides the information requested.

Question 001.378 (5.2.1.1)

Provide the analyses showing the assumptions made for the meltdown statement, such as temperatures, heat paths, convective cooling paths and creep assumption, to support the statement that the core support structure will last for 300 hours.

Response:

The meltdown statement referred to in the question has been deleted since that requirmeent will not be placed on the reactor vessel.

The information requested in this response is provided in revised Section 5.2.1.1. 25

Question 001.379 (15.0)

Provide the number and size of the reactor head and vessel support bolts as well as the high strain rate data for the materials used in the hold down bolting system.

Response:

As indicated in Section 5.2.2 and CRBRP-3, Volume 1 (Reference 10a of PSAR Section 1.6) the reactor vessel support design has been modified, replacing the bolted attachment of the reactor vessel flange to the support ring with an integrally welded attachment.

The closure head assembly which consists of the three rotating plugs, interconnecting risers, and attached components, is mounted to the reactor vessel flange via the outer risers. In addition, the plugs and flange are interconnected by shear rings which would transfer upward loadings directly to the flange, in lieu of through the riser assemblies.

The entire reactor vessel/closure head assembly is secured to the reactor support ledge of the reactor cavity by a row of bolts through the support ring. These bolts would experience tension due to upward loadings on the closure head. The design specifies 69 hold down bolts fabricated from SA193 Class B7 (120,000 psi yield strength) with a total cross section of 415 square inches. The bolt system remains elastic during the SMBDB loading. Therefore high strain rate data are not required.

Question 001.380 (15.0)

Provide sketches and/or drawings of the details of the reactor inlet and outlet nozzles indicating the clearance between the inlet and the outlet piping and associated guard piping.

Response:

Figure 5.2-1A specifies the clearance between the bottom OD of the vessel piping and the bottom ID of the guard vessel to be 2.00 inches minimum. This minimum clearance is required at full power operating conditions. For further details on the inlet and outlet nozzles see Figure 5.2-1B. The reactor vessel inlet pipe will have an OD of 24.00" and the reactor vessel outlet pipe will have an OD of 36.00", whereas guard vessel inlet pipe will have an ID of 38.00" and the guard vessel outlet pipe will have an ID of 50 inches.

Question 001.381 (9.1.3.1.2)

It would appear that a common mode failure effecting the sodium and NaK pumps in the EVST cooling system could make the system inoperable. If this was to occur, how much time is available before boiling commences in the EVST, under the most severe conditions, and what are the consequences? Justify your position that a single pump in each circuit provides adequate reliability for this important system.

Response:

The safety evaluation of the EVST cooling system in Section 9.1.3.1.3 discusses the safeguards incorporated in the cooling system design which preclude the possibility of a common mode failure rendering all three cooling loops inoperable. No credible common mode failure including a complete loss of all off-site and on-site power can be identified for total interruption of EVST cooling.

Each of the two normal EVST cooling circuits is normally supplied by off-site power. In case of off-site power failure, the normal cooling circuits are served by the two standby (diesel) AC-power sources. Each normal cooling circuit is connected to a different standby power source.

The exceedingly low probability of a complete loss of all AC-power is evident from the diversity and redundancy of power supply sources discussed in Chapter 8. Subsequent to the asking of Q001.381, a third cooling loop was added to the EVST. This loop was added so that even in the extremely unlikely event of a failure of both normal EVST cooling loops, EVST cooling will be maintained by operation of the third (backup) natural convection cooling circuit. This circuit consists of natural circulating sodium, NaK, and air to remove EVST decay heat of 1800 kW while maintaining sodium temperature below 775^{o}F. This loop requires no electric power for operation and does not contain pumps. 44

Question 001.382 (9.1.3.1.5)

Leak detectors are not mentioned for the EVST. Will they be provided and are they maintainable between the EVST and the guard vessel?

Response:

Sodium leak detectors will be provided in the EVST. This is stated in Section 9.1.2.1.2. The leak detectors proposed are two sodium aerosol/vapor monitors, and two liquid sodium conductivity detectors.

Aerosol monitors described in Section 7.5.5 are connected to two gas suction tubes which are installed in the inerted, annular gap between the EVST storage and guard vessels at different circumferential positions. They terminate each at a different level near the bottom of the cylindrical section of the EVST.

No maintenance is required for the gas sampling tubes. The gas sampler is in a local panel accessible for routine maintenance.

The conductivity detectors are described in Section 7.5.5. They are located at the lowest point of the concave part of the guard vessel bottom. Two guide tubes at different circumferential positions extend from the bottom of the guard vessel to accessible locations at the top of the EVST. The guide tubes serve for insertion and removal of the conductivity sensors and their cables. The condition of the installed detectors and their circuits is continuously monitored electrically to assure that all connections and wiring are in working order. In the event of detector failure, the detector is pulled out and a new detector is inserted in the guide tube.

Question 001.383 (9.1.3.2)

What safety procedures would be initiated if a leak developed in the fuel handling cell spent fuel storage tank blocking the flow of argon cooling gas between it and its guard vessel?

Response:

The information requested in this response is provided in revised Section 9.1.2.2.3. 25

Question 001.384 (9.1.4)

If a CCP was dropped, even though it was prevented from falling over horizontally by the EVTM, could it be picked up again by the EVTM? If not, how long would it take to boil sodium in the CCP with the hottest fuel subassembly? Discuss the consequences of such an event.

Response:

The information requested can be found in revised Section 9.1.4.3.3. 25

Question 001.385 (9.1.4.3)

Discuss how sodium and fission products are removed from the drip pan of the EVTM. Is there a possibility of a fire in the drip pan during the travel of the EVTM in the RCB or RSB?

Response:

Revised Section 9.1.4.3.2 provides the requested information. 25

Question 001.386 (9.1.4.3.3)

Reference and provide reports in which the similarities of the FFTF and Hallam Fuel Handling mechanism are compared to those to be employed in CRBR and elaborate on actual test or operating experience with these mechanisms in a similar sodium and cover gas environment and temperature.

Response:

No reports exist in which the similarities of the CRBR Ex-Vessel Transfer Machine (EVTM) and other ex-vessel fuel handling machines are compared. Accordingly, the reference to the Hallam Fuel Handling mechanism has been deleted from Section 9.1.4.3.3. As discussed in PSAR Section 1.5.2.7, test results will be used to show the adequacy of EVTM heat removal capability. After the detailed design of the EVTM is developed, the mechanisms chosen as components of the EVTM will be appropriately qualified to ensure their ability to operate in the required environment.

Question 001.387 (9.1.4.4.2)

In the event that invessel transfer machine mechanism malfunctions and one has to go to a manual operation, what is the maximum temperature attained by the fuel cladding, for the hottest element, for the most severe situation, during the manual transfer operation? Say, with a hot fuel subassembly out of the reactor sodium but not in the invessel transfer machine, with little or no cooling of the CCP.

Response:

Revised Section 9.1.4.4.2 provides the information requested. 25

Question 001.388 (9.1.1)

Locate on Figure 9.1-2 the contingency storage area for new fuel and describe how new fuel would be handled (movement paths) to utilize this storage area.

Response:

The contingency storage area identified in Figure 9.1-1 has been deleted from the Refueling System design due to system design refinements. Figure 9.1-1 has been modified to reflect this design change.

Question 001.389 (9.1.2)

Discuss the extent of fuel examination contemplated in the FHC. Will fuel subassemblies be taken apart in the FHC? If only the exterior surface of the duct is to be examined, how will the sodium film be removed? Discuss the purpose and expected frequency of the examinations planned.

Response:

Section 9.1.2.2. has been expanded to discuss spent fuel examination. 25

Question 001.390 (9.1.2.1.3)

Provide the analysis showing that the EVST top cover can absorb the heaviest drop loads carried above it without changing the lattice spacing of the fuel storage tubes. List the weights of the heaviest objects that may be carried over the EVST.

Response:

The upper surface of the EVST closure head assembly consists of a 6.5-inch thick striker plate, supported at its periphery (see Figure Q001.390-1). The striker plate forms part of the RSB operating floor and protects the underlying thick steel closure head by absorbing all normal and off-normal structural loads, including accidental impact loads. The distance between the lower surface of the striker plate and the upper surface of the steel closure head is about 10 in.

The heaviest load normally carried over the EVST is the EVTM floor valve (9 tons). The lift height of the EVTM floor valve is limited to 2 feet. No heavier loads which might be carried over the EVST can be identified. The spent fuel shipping cask, for example, is being transferred only between the cask shaft and a railroad car; both facilities are more than 20 feet distant from the periphery of the EVST. All heavy maintenance equipment is transported by the large component transporter (LCT) between RSB and RCB (see Section 9.2.1.2.2). The heavy maintenance equipment and the LCT are handled only by the double reeved main hook of the RSB bridge crane, and they are not carried over the EVST. Seismic restraints prevent equipment loaded on top of the LCT from toppling onto the RSB operating floor or EVST during an earthquake.

In spite of these considerations, a hypothetical heavy weight due to maintenance operations has been postulated to drop onto the EVST. The 25-ton load limit of the RSB bridge crane auxiliary hook was selected as hypothetical weight for this "umbrella" event. A drop height of 2 feet above the EVST striker plate was assumed. This represents the maximum handling height above the operating floor during maintenance operations.

Stress calculations were performed to determine the maximum deflection of the striker plate, and the maximum stress due to the postulated impact load. The analysis was based on the following ground rules:

(1) The load drops onto the center of the EVST striker plate with a load impact area of 1 ft^2.

(2) The impact load was converted to an equivalent static force, using a spring constant which takes the presence of inspection holes and fuel transfer port holes in the striker plate into consideration.

(3) Conventional flat plate formulas for deflection and stress were used.

The results of the analysis are as follows:

(1) The weight of 25 tons dropping 2 feet is equivalent to a static force of 1.15×10^6 lb acting on the striker plate center.

(2) Due to this force, the striker plate would experience a maximum deflection of 3.93 inch into the air space between striker plate and steel top shield. This amount of deflection is not sufficient for the striker plate to touch the underlying closure head, therefore, the impact load would not be transmitted to the steel closure head.

(3) The maximum striker plate stress due to the impact load is about 13,000 psi, which compares to a minimum yield strength of 35,000 psi for the striker plate material.

From the above considerations, it was concluded that the accidental drop of the heaviest object carried over the EVST could not lead to a change in lattice spacing of the fuel storage tubes.

Question 001.391 (9.1.2.2)

Discuss how you propose to maintain equipment in the FHC and how this cell would be decontaminated of alpha particles after handling failed fuel assemblies.

Response:

The information requested can be found in revised Section 9.1.2.2. 25

Question 001.392 (9.1.2.2):

When new fuel, at ambient temperatures, is inserted in the EVST the fuel subassemblies could be subjected to a thermal shock. Discuss how this is controlled and what stresses occur in the fuel subassembly, and where the most severe loads are and do they approach the yield point of the material?

Response:

The purpose of the gas filled (dry) preheat stations in the EVST, mentioned in Sections 9.1.2.2 and 9.1.4.1, is to provide a slower heatup rate for new fuel assemblies than would be achieved by direct immersion into sodium.

The preheat stations are thimbles filled with EVST argon cover gas. The gas is in thermal equilibrium with the sodium surrounding the thimbles. New Fuel assemblies are inserted into the thimbles with a lowering speed of 2 ft/min.

Heat transfer calculations using the computer code DEAP (Differential Equation Analyzer Program, described in Appendix A of the PSAR) with conservative assumptions were performed to obtain temperatures gradients in a new fuel assembly during preheating. The new fuel assembly was assumed to have an initial temperature of 70°F, and was suddenly subjected to a 425°F temperature step due to full submersion in the argon gas of the preheat thimble.

The results of the analysis indicated that a maximum temperature difference of about 350°F will occur between the center fuel rod and a fuel rod in the outer row, located near a corner of the hexagonal fuel assembly duct. The maximum rate of temperature increase will occur in the outer fuel rod and will be 12.5°F/min. The maximum temperature gradient through the fuel cladding is less than 10°F/in. in radial direction. This gradient is two orders of magnitude less than a local temperature gradient of 1200°F/in. required to induce thermal stresses which would exceed the material design stress of 40,000 psi.

It is concluded that immersion of new fuel assemblies in preheat stations does not impose any severe thermal loads to the cladding.

Question 001.393 (9.1.2.2.1):

Since it is expected that the FHC will be contaminated by alpha, how is the spread of alpha contamination controlled when the cell plugs are removed into the RSB which is also open to the RCB during fuel handling, and to the atmosphere through the H&V system? It is realized that a floor valve and transfer machine is used but it does not seem likely that 100% control of alpha particles is realistic.

Response:

Prior to fuel handling operations which involve transfer of fuel assemblies in and out of the FHC or EVST, the fuel transfer port plugs will be removed. This is accomplished using floor valves and the EVTM. The EVTM couples with its closure valve to a floor valve on top of a fuel transfer port and removes the port plug. During transport to the RSB port plug storage facility, the port plug is within the inerted containment boundry and a drip pan in the EVTM closure valve receives any sodium which might drip from the bottom of a port plug. After the EVTM has traveled to the inerted RSB plug storage facility, its closure valve is coupled to a floor valve and the plug is deposited into an empty position of the rotatable plug storage facility rack. During the plug transfer process and during the entire time of plug storage, no plug surfaces are ever in contact with the atmosphere of the RSB/RCB.

However, a small annular area underneath the EVTM closure valve and a matching surface on the floor valves could contain small amounts of contaminated sodium. The following administratively controlled, precautionary procedures are presently anticipated whenever the EVTM is decoupled from a facility with potential alpha-contamination, i.e., the reactor, EVST, or FHC. These procedures are preliminary, pending further refinement of the Reactor Refueling System equipment and facility design and their operation.

A. General Techniques for Contamination Confinement During Fuel Handling

1. The immediate area of the EVTM movement path is barricaded or roped off prior to any fuel or plug transfer. Only a mechanic and health physicist (HP) are present inside the barriers, specifically near the floor valves being serviced by a transfer machine. Both wear appropriate protective clothing and respiratory protection.

2. Before the mechanic can proceed with any hands-on operation involving potentially contaminated surfaces, the HP surveys the direct radiation and radioactive contamination of these surfaces and records the data. If the radiation contamination level is acceptable, he notifies the mechanic to proceed with the operation.

3. At frequent intervals the EVTM cask volume within the pressure boundary is purged with fresh argon from the floor

service stations. The contaminated gas is vented to the same stations. These stations are connected to the plant argon supply and processing systems.

4. Interface purging is performed by the EVTM before opening the closure valve gate to exclude air from the transfer passage. The interface is again purged after closure, prior to separation from the floor valve, to dispose of any contamination gas.

B. <u>Specific Procedure After Machine Decoupling</u>

1. The EVTM is uncoupled fully raising the extender.

2. The mechanic covers the floor valve with a plastic sheet and secures it to the valve. This sheet is not removed until the next time the EVTM will mate again to the same floor valve. He then places a second plastic sheet on top of it. This will serve as a disposal bag after the next operation is completed.

3. The mechanic cleans the annular area of the closure valve, e.g., by wiping it with alcohol dampening swabs. He then puts the used swabs onto the second sheet on top of the closure valve, and closes it forming a disposal bag around the swabs and any drippage. Later the bag will be transferred to the Radioactive Waste System in a drum or other appropriate container.

4. Finally the mechanic covers the cleaned lower surface of the closure valve with another plastic sheet and secures it to the closure valve.

5. The EVTM is moved to its next location. There the plastic cover sheet is removed from the closure valve before coupling to the floor valve. The removed cover sheet is transferred to the Radioactive Waste System.

The following three items will all contribute to minimizing the occurrence and/or spread of alpha-contamination during fuel handling and storage:

1. Close monitoring for failed fuel, as discussed in Section 7.5.4.

2. Transferring and storing all components with potential alpha-contamination in a sealed, inerted atmosphere, as described in Section 9.1.

3. Following carefully planned and administratively controlled procedures, such as outlined above, whenever a transfer machine is decoupled and moved from a facility with potential alpha-contamination.

Question 001.394 (9.1.2.2.1)

What controls are proposed on the building H&V System during fuel handling? Is full ventilation air continually exhausted to atmosphere from both buildings? Discuss what precautionary measures are taken during this time, if any.

Response:

The response to this question is incorporated into revised PSAR Sections
49 9.6.2.2.1 and 9.6.3.2 and into revised Figures 9.6-5, 9.6-7 and 9.6-8.

Question 001.395 (9.1.2.2.2)

What maximum temperature does the Dowtherm attain during loading of the spent fuel cask, what is the vapor pressure of Dowtherm at this temperature, and what is its flash point? Discuss the fire potential in the fuel shipping cask loading operation.

Response;

Revised Section 9.1.2.2.2 provides the information requested. 25

Question 001.396 (9.1.4.4.2)

Discuss how the maximum force required to remove spent fuel is related to bowed subassemblies, and relate this to burnup and core position. What maximum force can a spent fuel subassembly withstand without failure? Can the pulling force be transferred to the pins, and what force can they withstand at end of life postulating that a duct may have cracked during the fuel removal operation.

Response:

Revised Section 9.1.4.4.2 contains the discussion requested.

Question 001.397 (9.2)

The Sodium and Decontamination System is located in the RCB and it is proposed that alcohol may be used as a cleaning agent. How much alcohol will exist at any given time in the RCB? Where will it be stored, and how will it be used?

Response:

This question is no longer applicable. There is no planned use of alcohol for sodium removal in the Nuclear Island and the statement mentioning that process has been deleted from Section 9.2.2.2. The Water Vapor-Nitrogen (WVN) process has been selected as the reference sodium removal process.

Question 001.398 (9.2):

Discuss the potential for release of trapped fission products present in the sodium into RCB associated with cleaning a large component in which hydrogen explosion might occur.

Response:

The uncontrolled or runaway reaction of sodium with water and resultant hydrogen generation in the Large Component Cleaning Vessel (LCCV) is defined as an "extremely unlikely event". The postulated accident would be a result of a failure, malfunction, or operator error but nevertheless can be used to evaluate the design basis for the vessel. The response to question 001.201 describes how regulation of water vapor concentration in the water vapor nitrogen (WVN) mixture introduced in the cleaning vessel and regulation of nitrogen purge rate will control the hydrogen concentration in the Primary Sodium Removal and Decontamination (PSR+D) System. The response also provides information on the monitoring of any H_2 concentration and actions should a high H_2 concentration be indicated.

Detailed information is provided in new PSAR Section 15.7.3.7.1.

Question 001.399 (9.3.4)

The table identifying leak detectors shows an aerosol type detector for the cold trap cells. There is no indication of a leak detector on the cold traps themselves. Justify the absence of leak detectors on the cold traps to indicate leakage as quickly as possible since the primary cold traps are a potential large source of radioactivity.

Response:

All liquid metal containing systems (piping, vessels and equipment) will be provided with adequate leak detection to meet plant design and applicable regulatory requirements. The preliminary design of the liquid metal leak detection system, including the quantity, type and location of leak detectors was provided under separate cover in September, 1976. Table 9.3-4 indicates planned location of leak detectors for the auxiliary liquid metal systems. Furthermore, radial monitors are planned to be located in the cells containing primary system piping and components to detect leaks of any radiological significance. Nothing contemplated would preclude the addition of leak detectors on the cold traps themselves if it becomes necessary to do so at a later date.

Question 001.400 (9.4.2)

Discuss what precautions are being taken in the location of heating elements and their control and rate of heating to assure that excessive thermal gradients and stresses do not occur during preheat with empty piping.

Response:

The requested information is provided in revised Section 9.4.2.

Question 001.401 (9.5.1.1)

Provide your analysis to show that the head access area does not exceed 1/10 MPC considering back diffusion through the buffer gas, using the highest specific activity in the cover gas and the lowest dilution rate by the H&V system.

Response

Preliminary estimate of total cover gas leakage through all seals of the reactor vessel head and head mounted equipment is 0.012 scc/min. In the calculation of radionuclide efflux shown in Table 12.2-1, each isotope was conservatively assumed to be present in the leaked gas at the same concentration as in the cover gas (Table 11.3-2). Calculation of head access area (HAA) radionuclide concentrations given in Table 12.2-2 was based on the efflux of Table 12.2-1 and assumed that this was uniformly mixed with and diluted by 12,000 cfm of once-through ventilation air. Credit was taken for decay of radionuclides within the HAA after relaese from the seals. The sum of the nuclides is 0.075 times MPC as shown in Table 12.2-2.

This calculation is quite conservative in that it takes no credit for decay of radionuclides prior to release in the HAA. Actually all of the paths from the cover gas space have buffer volumes with residence times which are many half lives for most of the isotopes. This allows radioactive decay to take place, thus reducing radioisotope content below the assumed concentrations before release of the leaked gas to the HAA.

Question 001.402 (9.5.2.1)

Elaborate on the sodium fire control system in which it is stated that 2,500 scfm can be provided to any one of eleven cells in the intermediate bay. Are these cells designed to withstand the increase in gas pressure resulting from gas inflow, products of combustion, and the heat added to the mixture?

Response:

The nitrogen flooding capability has been eliminated from the fire protection system. PSAR Section 9.13.2 provides a complete description of the Sodium Fire Protection Systems and Section 15.6.1.5 provides an evaluation of the plant capability to withstand the effects of a sodium fire in the IHTS cells.

Question 001.403 (9.5.2.5.1)

It is not clear from the discussion of item (c) what the response of the CAPS system is under a high purge rate. Discuss the relationship between these two systems.

Response:

Paragraph 9.5.2.5.1 refers to instrumentation requirements for the nitrogen distribution subsystem. Item c delineates the need for control of pressure and/or flow to various components, including the RAPS and CAPS cold boxes. Section 9.5.2.2. has been expanded to provide the requested information. 25

Question 001.404 (9.4)

Discuss what measures have been taken in the chilled water system to prevent water contact with sodium equipment through leaks in piping or transport of moisture or water through ducting?

Response:

The information requested has been provided in revised Section 9.7.3. 15

Question 001.405 (9.6)

Justify the open containment concept for both the RSB and the RCB as being consistent with the low as practicable objective and providing defense in depth for the design basis accident yet to be defined for CRBR. It would seem that the H & V system provided has the prime objective of providing a dilution mechanism for the escaped radioactivity from the reactor and defeats the concept of confining radioactivity for treatment, decay, and later disposal. Secondly, the open containment concept becomes an active system rather than a passive system depending upon the closure of dampers and valves to become effective rather than the reverse situation in which no active action has to occur to provide containment. Justify your selection of this containment concept.

Response:

The CRBRP containment is designed and the plant operating philosophy is developed on the basis of an open containment. The design of the containment and the containment ventilation system is provided with sufficient safeguards to prevent accidental release of radioactive materials.

The presently identified steady state release of radioactivity during plant operation, identified in PSAR Section 11.3, results in effluents and associated doses orders of magnitude lower than the levels of 10OFR20. Section 11.3 lists the total annual gaseous effluent release for CRBRP as 710 Ci/yr, compared to the minimum total gaseous effluent release of LWR's studied in WASH-1258 of 3600 ci/yr.

PSAR Section 11.3 lists the integrated dose to the population within 50 miles of the CRBRP site in the year 2010 as 1.7×10^{-2} man-rem/yr. The added cost of a closed containment over the reference design open containment cannot be justified from a cost benefit analysis because of the low operational releases from CRBRP. An integral part of the CRBRP design is to allow personnel access during normal plant operation, in order to ensure equipment operability and to perform routine operations of equipment located within the containment. This equipment is located in containment so that it is closer to the primary system equipment it serves. (Examples are sodium sampling, inservice inspection, access to I&C cubicles, and Large Sodium Component Cleaning Vessel use.) The airborne activity would be too high to allow continuous occupancy during operation if the containment were not purged (open containment).

RCB

In order to provide a very low leakage barrier at the primary containment boundary, a seismic Category I, tornado hardened concrete confinement structure is provided around the outside of the inner steel containment vessel with an annular space separating the two structures.

The annular space between the inner and outer containments will be maintained at a negative pressure relative to atmospheric pressure during normal operation and accident condition and is exhausted through high efficiency filters. The filtered exhaust point is chosen to obtain maximum dispersal of the radioactive material prior to reaching the Control Room intake. In addition, the recirculation system for the Control Room atmosphere is increased in capacity to 8500 cfm.

The containment atmosphere ventilation system is reduced in capacity from 50,000 cfm to about 14,000 cfm in order to minimize the potential release of activity from containment during valve closure time. The containment supply and exhause penetrations will be reduced from 48" to 24". Containment isolation of the HVAC system exhausts will be designed to meet item 4 of the CRBR Design Criteria 47. The containment ventilation/purge system is provided with a time delay duct to prevent the release of radioactive materials during accident conditions. The time delay duct is sized for such velocity, that the containment isolation valves will close before the contaminated air reaches the valve zone. Radiation monitors which provide signals for initiating closure of the containment isolation valves are provided at the inlet of the time delay duct and in the HAA.

During normal plant operation and all accident conditions, the containment/confinement annulus space is maintained at a minimum 1/4" water gauge negative pressure with respect to the outside atmosphere. During normal plant operation, the RCB Operating Floor is maintained under slight negative pressure (<1/8" water gauge). Capability is provided to filter the containment/confinement annulus exhaust through the annulus filter units during normal plant operation and all accident conditions. The filter system will consist of two 100% redundant filter-fan units consisting of prefilter, demister, heating coil, HEPA filter bank, absorbent filter bank, after HEPA filter bank and fan components (approximately 14,000 CFM capacity).

A tornado missile protected, Seismic Category I enclosure is provided for the RCB annulus filter-fan units, the RCB normal exhaust fans and the annulus pressure maintenance fans. Shielded wall partitions are provided in the HVAC equipment room between the redundant annulus filter-fan-units, RCB exhaust fans, and the annulus pressure maintenance fans. Tornado missile protected, Seismic Category I air intake and discharge openings are provided.

RSB

The design for the RSB is described in PSAR Section 3.4 and analyzed in Section 15.6. The resulting doses are significantly below appropriate 10CFR100 guidelines values and meet or exceed all of the Design Criteria specified in PSAR Section 3.1. However, modifications to the RSB HVAC system were made to limit air infiltration and to provide recirculation and filtration capabilities during all operating conditions as discussed in Section 9.6-3.

Question 001.406 (5.3.1.1, 5.4.1.1, 5.5.1.1)

For the design bases stated for the PHTS and the IHTS, the performance objectives include operation of these systems to remove decay heat by natural circulation, as identified in item (c) of these sections. Moreover, in Section 5.5.1.1, the performance objectives of the steam generator system includes removal of plant sensible heat as well as reactor decay heat under natural circulation conditions, as indicated in item (d). Specify whether the CRBR plant design will be committed to make these performance objectives into design requirements for the CRBR plant. Identify the applicable experimental data and/or R&D program on which the analytical methods would be based to establish the natural circulation capability for the removal of sensible and decay heat from the reactor.

Response:

The CRBRP project is committed to natural circulation decay heat removal as a design requirement from rated thermal power (975 MW_t) on three loops. The plan to establish the capability for removal of sensible and decay heat via natural circulation is defined in a report titled "Verification of Natural Circulation In Clinch River Breeder Reactor - A Plan", which has been supplied under separate cover.

Question 001.407 (5.5.2.6)

Inasmuch as sodium slugs can be injected into the piping system from the rupture discs, which can lead to high reactive forces and possibly sodium-hammer impacts on the system, provide the criterion or the basis for the design of the piping sizes and system to the separation tank to preclude SWRPRS failure and the subsequent formation of a potentially explosive bubble of hydrogen in the steam generator cell atmosphere. In the event of failure under these circumstances discuss the possibility of the consequences exceeding those estimated for the intermediate heat transport system pipe leak discussed in Section 15.6.1.5, because of the explosive capability of the hydrogen potentially leading to the propagation of structural damage.

Response:

The basis for design of the SWRPRS piping and components, with special consideration of the system components located within the steam generator building, have been selected to preclude a rupture of the system for sodium water reactions up to and including the Design Basis water to sodium leak (DBL). The design is based upon the loadings predicted within the system by analysis of the heat transport system and the SWRPRS by the TRANSWRAP code. Analysis of the conditions in SWRPRS following the DBL show that the greatest loads result from forces created as the slug of sodium is forced through the pipe from the Steam Generator Module to the Reaction Products Separator Tanks (RPST).

The high velocity of the sodium slug as it traverses the pipe and is deflected around the bends at the pipe elbows creates the loads which represent the basis for the piping and RPST design. These faulted loads are combined with those of the SSE in order to provide a degree of conservatism consistent with the intent of Regulatory Guide 1.48.

Assumptions made for the TRANSWRAP analyses for the CRBRP include a high degree of conservatism in establishing the velocities and the gas pressure at the leak location in the faulted steam generator module. The most significant assumptions which maximize the volume of hydrogen gas assumed to be generated by the TRANSWRAP analyses during the interval that the sodium slug is being accelerated from the steam generator into the RPST are assumptions a and d of Section 5.5.3.6.2 as follows:

a. Instantaneous conversion of all water injected to hydrogen gas as represented by the reaction: $H_2O + 2Na \rightarrow Na_2O + H_2\uparrow$

d. There is no heat loss from the reaction products to the surrounding structures.

Assumption a introduces a significant margin into the calculation by assuming full release of the hydrogen from the reaction between water/steam and the sodium. Test data indicate that the hydrogen release is approximately 55% of the total hydrogen in the reaction. The analysis also assumes that the water/steam reacts as soon as it has passed beyond the failed tube at the location postulated for the leak. For the DBL, a hydrogen gas bubble will be formed at the location of the leak which pushes the mass of sodium away from the leak location. The assumption of instantaneous conversion of water to hydrogen is conservative because of the transient time for the water/steam to penetrate through the hydrogen bubble and reach the sodium. The conservatism which might be attributed to this has not been estimated.

Assumption d results in hydrogen gas bubble temperature of about 2500F. The steam generator structure and IHTS piping and essentially all of the sodium mass in the steam generator module will be in the temperature range from about 650F to 950F. The mass of this structure, piping, and sodium is several orders of magnitude greater than the mass of hydrogen generated during the DBL steam generator failure. This hydrogen will be relatively quickly cooled to near the temperature of the metal structure and sodium with which it is in contact. The reduced temperature will reduce the effective pressure-volume of the hydrogen in the driving hydrogen bubble significantly from that assumed in the TRANSWRAP analysis.

Reference 12 of Section 5.5 provides detailed information regarding the LLTR test program which includes as test objectives, the determination of the adequacy of the TRANSWRAP analysis procedures and the effectiveness of the sodium-water reaction pressure relief system as adapted to that test system.

Because of the conservatism discussed above the release of hydrogen resulting from a failure of the SWRPRS is not a design basis for CRBRP.

Question 001.408 (E.2.2)

Provide the technical basis that structural integrity of the reactor vessel is maintained in the event of a double-ended pipe rupture in the PHTS creating near boiling conditions in the core.

Response:

Revised PSAR Section E.3.1.2.2 discusses the structural integrity of the reactor vessel in the event of a double-ended pipe rupture.

Question 001.409 (E.2.0)

Provide the basis for not considering the accommodation of a spectrum of sodium pipe ruptures (including the double-ended rupture) in the IHTS and SGS cells. In addition, confirm whether or not these cells will be vented.

Response:

A spectrum of sodium pipe ruptures (including the double ended rupture) in the Steam Generator Building are being considered in the building design bases. The enveloping cases and the effects of the sodium spills are currently being determined. In general, the plant could accommodate major spills by tripping the reactor and removing decay heat via the unaffected loops of the IHTS, SGS and SGAHRS. The cells in the SGS will be vented (between cells within each loop and to the environment) to maintain the internal pressure within the capability of the cell walls.

Question 001.411 (E.3.1.3.1)

Confirm whether or not the dimensions of the reactor guard vessel change from the reference design in order to accommodate the pipe sleeve. In addition, provide the technical justification to support the conclusion that the sodium level will not be such as to uncover the reactor outlet nozzle during a double-ended pipe rupture transient.

Response:

The reactor guard vessel inlet pipe nominal I.D. has to be increased from 38 inches in the reference design to 54 inches in order to accommodate the pipe sleeve. This increase in guard vessel pipe size results in an increase in the guard vessel annulus volume from 2855 cubic feet to 3183 cubic feet. The larger guard vessel pipe size also requires a lowering of the guard vessel skirt to vessel attachment point. However, this change will not affect the guard vessel annulus volume.

For the Parallel Design with the pipe sleeve around the primary cold-leg piping, the volume of the sleeve annulus is much smaller than the Reference Design guard vessel annulus volume. Thus, for ruptures within the sleeve the vessel sodium level will be maintained (with a comfortable margin) above the minimum safe level. For breaks in other locations of the primary pipe within the guard vessel, analysis shows that even with the guard vessel enlarged to accommodate the pipe sleeve and no pipe sleeve in place, the minimum safe sodium level in the vessel will be maintained. The interaction of plant features to ensure maintenance of the minimum safe sodium level (for the reference design) is discussed in PSAR Section 5.3.2.1.1.

Question 001.412 (E3.1.1):

On Page E.3-1, the reference to the definition of the radial blanket hot channel in section 15.2 is unclear. Provide the definition of the radial blanket hot channel.

Response:

In Section 15.2, as in Appendix E, the radial blanket temperatures described are for the hottest rod in the highest power radial blanket assembly. This highest power radial blanket assembly is identified as assembly A_1 on Figure 4.4-18 which has a peak linear heating rate of 17.6 kw/ft on a 3σ basis. As defined in Section 4.4.3.2, "The hot (or maximum) rod is the one in the assembly with the maximum cladding midwall temperature; due to the flow distribution in a wire wrapped assembly, generally it does not coincide with the peak rod. Hot channel/hot spot factors are added to the nuclear peaking factors characteristic of the hot rod. Hot rod temperatures at 0σ, 2σ or 3σ are generally quoted, depending on the selected degree of confidence". The hot channel/hot spot factors which include engineering and nuclear uncertainties are combined semi-statistically (see Section 4.4.3.2). For transient studies such as those in Appendix E and Section 15.2, maximum temperatures are calculated at the worst time in life for the hot rod on a 3σ basis using hot channel/hot spot factors which are given by Table 4.4-5 and described in Section 4.4.3.2. The reference in Section E3.1.1 has been revised for clarification.

Question 001.413 (E3.1.3)

"Discuss the sensitivity of the calculated coolant temperatures and saturation temperatures to the scram signal delay time for different trip functions."

Response:

The requested information is provided in Section E3.1.3.1 of the PSAR.

Question 001.414

The current pipe-sleeve analysis performed with the DEMO Code appears to depend on the ability to calculate differences in temperatures as low as approximately 50° between the coolant and saturation temperatures. Unless the uncertainty in the current DEMO analysis cannot be established to be significantly less than the presently calculated temperature differences, the margin provided by the pipe-sleeve concept for the accommodation of pipe rupture cannot be adequately quantified. Additional information and/or R&D efforts are necessary to resolve such uncertainties and to confirm that such margins can realistically be expected. Include in your response a discussion of the status and content of the optional pipe-sleeve flow test program indicated on Figure E.4-1.

Response:

The NRC staff has stated in the July 9, 1976 ACRS meeting that the Project need not consider a double ended rupture of the primary cold-leg piping as a design basis event. This NRC staff position is contingent on the Project providing an adequate pre-service/inspection program, a material surveillance program, material performance verification program and a verification program for the leak detection system. Formal documentation of the NRC stated position is expected to be issued to the Project in the very near future. The Project expects that full agreement can be reached between the Project and the NRC staff on the conditions used as the basis of the NRC position. Therefore, the Project will cease activities on the Parallel Design Pipe Rupture Accommodation, which includes the pipe-sleeve flow test program and will focus on the Reference Design Basis Leak.

Not withstanding the above, to respond to the first part of the question concerning DEMO, the pipe sleeve analysis presented in Section E.3.1.3 was performed using a conservatively biased set of parameters to include uncertainty (noted in Table E3-1.) The conservatism applied to the analysis is discussed in the response to Question 001.419. The Thermal-Hydraulic Design operating point from which the pipe sleeve analysis was initiated also has a conservative basis described in the response to Question 001.318 (Section 15.3). Calculational uncertainty in the DEMO Code is judged to fall well within the bounds of the above conservative biasing, and a margin of 50°F is considered sufficient to show adequate core cooling.

Question 001.415 (E3.1.3)

Discuss the possibility that other criteria besides coolant boiling may also have to be established to insure a coolable geometry taking into consideration possible clad deterioration and the possibility of clad rupture due to fission gas pressure at EOEC.

Response:

The core damage criterion for primary pipe rupture is that loss of coolable geometry shall not occur as a result of the event. As discussed in Section E2.2, this criterion is interpreted as a requirement to maintain the hot channel coolant below saturation conditions. This will assure that no cladding melting and relocation occurs. For cladding melting to occur during loss of cooling events, the cladding must be dry. Therefore any effect which may be hypothesized to cause cladding melting must evaporate the adhering liquid film from the cladding and then maintain channel conditioning greater than saturation. A mechanism that could be postulated to increase the sodium residence time in the core due to partial voiding of the channel is cladding rupture (due to cladding deterioration and fission gas pressure) and subsequent fission gas release. This is discussed below.

Figure 4.2-21 of the PSAR shows the transient limit curve for the top of the fuel stack for the hot fuel pin in CRBRP assembly 6 due to the loss of flow event illustrated in Figure 4.2-20. This shows that at the end of life, 80 Mwd/kg, corresponding to 411 full power days, the coolant temperature must exceed about 1600°F for cladding failure to occur. This considers cladding deterioration and the fission gas pressure increase due to increasing sodium temperature. The curve intersected 1600°F at 411 full power days which represents the design basis lifetime for worst case conditions and worst material properties. However, the loss of flow transient represented in this analysis is relatively slow. To better assess the consequences of a rapid transient in which the cladding is expanding away from the fuel, the transient limit curve for a fast reactivity insertion with zero contact pressure was estimated from Figure 4.2-22. This permits comparison of the effects of creep rupture damage with plastic damage (ultimate residual strength of the cladding). The failure temperature corresponding to 411 full power days was also about 1600°F. This indicates that the LOF transient in Figure 4.2-20 is still sufficiently rapid that creep rupture effects are not significant.

The core exit coolant temperatures for a double-ended pipe rupture with a sleeve, Figure E.3-3, are about 1710°F. However, these hot pin temperatures correspond to BOL pin power ratings for which Figure 4.2-22 shows that failure is not expected to occur. (It should be noted that the assumptions for BOL and EOL conditions are more conservative than BOEC conditions.) When adjusted for EOL fissile depletion (80 Mwd/kg) the corresponding coolant temperatures are conservatively estimated to be about 1580°F and cladding failure is not expected. However, it is noted that even if 1600°F would be exceeded, this would indicate that one fuel pin may fail, not that a gross number of pins would fail.

The failure temperature in the blanket is expected to be about the same as that in the core, namely 1600°F. However, the most probable failure location would be be between core mid-plane and three quarters up the total pellet stack height. Figure E.3-4 indicates that for double-ended rupture with a sleeve the coolant temperatures do not exceed this value. Furthermore, if cladding failure should occur, the failure is probably a considerable distance from the gas plenum and the resultant friction in the gap would preclude rapid fission gas egress (see similar discussion for fuel pin in Section 15.4.1.1.2).

It is concluded that, for loss of coolable geometry to occur for a pipe rupture transient coolant boiling must be initiated. Loss of cladding integrity resulting in fission gas release at EOL is not a mechanism which could cause coolant boiling during pipe rupture events with a sleeve design.

Question 001.416 (E3.1.3)

Provide representative transient results of the important parameters such as the variation as a function of time of coolant temperature, flow rates, local pressure, and possibly other significant variables as a function of time to illustrate the physical behavior of the system. For these transients select a pipe break location that leads to exceeding the saturation temperature, one that results in incipient saturation, and a location that results in temperatures well below saturation. Provide results with and without the pipe sleeve.

Response:

23 Additional figures are provided in Section E.3.1.3.4 for the requested transient results.

Question 001.417 (E3.1.3.1)

Identify the nature of the pump coastdown modification referred to in this section and provide the primary pump characteristics used in the analysis including the pump inertia, rated hydraulic and frictional torque pump head and torque versus flow at all pump speeds.

Response:

The modified pump coastdown referred to in Section E3.1.3.1 is the minimum - flow coastdown transient calculated by the DEMO Code for a minimum full-speed stored kinetic energy of 5.39 x 10^6 ft-lbs (as specified in Table E.3-1). The pump characteristics equations and coefficients for pump head, torque, frictional torque loss and RNPSH, as used in the DEMO - Rev. 4 analysis, are given in Table E.3-1A.

Question 001.418 (E3.1.3.1)

Provide the technical justification and supporting documentation for the use of the refined flux-to-(pressure)$^{1/2}$ trip function given in Table E.3-1.

Response:

The Flux - $\sqrt{\text{Pressure}}$ subsystem initiates trips for reductions in primary flow (or positive reactivity excursions) over the load range. Core inlet plenum pressure is a rapidly responding indication of core coolant flow which would drop rapidly following a pipe rupture, initiating a trip. The subsystem performance is dependent upon initial operating level and is modeled with a dynamic trip equation as shown in Table E.3-1. The model chosen includes worst case errors for the sensors, signal conditioning electronics, comparators and logic. For additional conservatism errors are then accumulated in the direction to delay a trip signal when needed by summing the conservative errors in the worst safety case direction. Using the sum of conservative errors rather than balancing the errors about the mean adds additional conservatism to the analysis. The analytical model of the Flux - $\sqrt{\text{Pressure}}$ subsystem shown in Table E.3-1 conservatively represents the Plant Protection System performance in response to the events analyzed in this Appendix. A discussion of the methods used to derive PPS trip equations will be provided in the response to Question 222.65.

Question 001.419 (E3.1.3.2 and E3.1.3.4)

With regard to the results presented in Figures E.3-1 to E.3-4 inclusive:

a) It is noted that if the uncertainty in the calculated coolant temperatures were in the order of ± 10%, the break location at which the coolant temperatures exceed the saturation temperature appears to be affected substantially. For example, for the core hot channel the break location may shift approximately 60 ft. from around the top of the downcomer to the vicinity of the check valve exit. In view of this sensitivity provide estimates of the uncertainties in the calculation of the coolant temperatures. These uncertainties should be those associated with parameter uncertainties within the DEMO Code, such as variations in gap conductance of about 30% as well as the uncertainty in the DEMO Code representation of the CRBR plant. Identify the existing or future efforts to validate the DEMO Code experimentally.

b) Provide the distance of the IHX inlet from the reactor vessel inlet in order to calibrate the curves measured from the reactor vessel inlet with those measured from the pump.

c) Identify whether the differences between the coolant temperatures and the saturation temperatures are the minimum values throughout the transient so that boiling does not occur before the temperatures shown in the figures are attained.

Response:

a) The loss of pipe integrity analysis was conducted with a set of parameters biased to include uncertainty. The uncertainties having principal bearing on calculated coolant temperature can be lumped into two groups, 1) those associated with the plant operating conditions and, 2) those associated with reactor power and thermal hydraulics.

The uncertainties in plant operating conditions are accommodated in the analysis by commencing the transient analysis from the Thermal Hydraulic Design conditions (T&H) plus 20° primary cold leg temperature. The conservativism of the analysis, commencing from this extreme operating point is discussed in NRC Response 001.318 (15.3). In that response the difference between T&H and expected peak temperatures, for a loss of off-site power, was shown to be 70°. This difference would be even larger for a pipe rupture since it is a more severe transient.

Reactor power and thermal hydraulics uncertainties are fully considered in the design and analysis through the effects of the hot channel factors which are discussed in Reference Q001.419-1. The result of the stacked uncertainties in the hot channel factors is shown in

Figure Q001.419-1. (Note that the analysis assumes single phase sodium flow up to 1950°F which is not realistic, but serves to demonstrate the margin resultant from stacking uncertainties). This analysis was done from the plant TDM condition for the hottest channel in the active core at steady state. The hot channel, which is used for pipe rupture analysis contains the hot channel factors of reference Q001.419-1, whereas the expected hot channel does not. As shown the hot channel factors provide 265°F of margin for the reactor uncertainties.

Validation of the DEMO Code for primary pipe rupture analyses has been performed by:

(a) Comparison of DEMO primary loop hydraulics against the IANUS Code (Westinghouse Proprietary) primary loop hydraulics, and

(b) Comparison of DEMO reactor thermal-hydraulic response with that of the FORE-IIM Code.

Since IANUS and FORE-IIM have been validated to the extent noted below confidence in DEMO validity has been gained.

1. DEMO - IANUS Hydraulics Comparison

Figure Q001.419-2 presents a comparison of the reactor inlet flow transient following a primary inlet pipe rupture, as calculated by DEMO and by IANUS. Since IANUS is configured for FFTF, this comparison was performed for FFTF parameters by adapting the DEMO hydraulics to represent FFTF. The slightly faster flow decay in the first half-second in IANUS is due to neglect in IANUS of the fluid inertia in the pipe from inside the vessel inlet plenum to the break. The close comparison provides a validation of DEMO hydraulic modelling and, since IANUS has been verified against the SEFOR tests (Reference Q001.419-2), further validity is provided for DEMO hydraulic modelling.

2. DEMO - FORE IIW Reactor Comparison

Figure Q001.419-3 presents a comparison of the reactor exit temperature response of the DEMO Code and the FORE IIM Code for a postulated inlet pipe rupture in the primary system. This comparison supports the validity of the DEMO core average, thermal-hydraulics and neutronics modelling. Figure Q001.419-4 is a comparison between DEMO and FORE IIM radial blanket calculated temperatures. This supports the validity of the radial blanket hot channel model. The FORE IIM core model has also been verified against the IANUS model by the FFTF Project.

(It is recognized that the DEMO results may not be realistic above the saturation temperature. However, the results shown in Figure Q001.419-4 are effective in demonstrating the validity of DEMO in the region below saturation temperature).

The DEMO Code has also been the subject of a careful independent review by the Argonne National Laboratory. The results presented in Reference Q001.419-3 support the validity of the fueled assembly hot channel thermal model.

In summary, the DEMO calculations have been and will continue to be examined for their appropriateness and accuracy. DEMO results have been compared to other calculations and indirectly to test data. This has given confidence in the codes basic calculational scheme and its ability to represent system and component real or required performance. On a continuing basis, as the design and construction progresses and component and system test data becomes available, applicable portions of the code will be compared to this information.

b) The analysis results presented in Figures E.3-1 and E.3-2 were plotted on separate scales to either side of the IHX primary as a consequence of the flow path complexity within the IHX. This complexity prevents a physically meaningful interpretation of distances through the unit (the same consideration was not applied to the check valve due to its nearly direct flow path). The total length along the pipe from the reactor vessel to the IHX outlet nozzle is very nearly 173 feet. The elevation difference from IHX outlet nozzle to inlet nozzle centerline on the primary side is 22 feet 7-1/2 inches, with the inlet nozzle 5 feet 4 inches from the IHX centerline. Use of these dimensions to interpret the net distance of pump-to-IHX break locations from the reactor vessel inlet should be limited to reference purposes only to avoid misinterpretation of path lengths within the IHX.

c) The temperatures plotted in the Section E3.1.3 analysis are the worst case temperatures at any point within the duration of the transient cases run; i.e., the temperatures plotted are the maximum sodium exit temperature along with the minimum saturation temperature occurring at the time of the peak in exit temperature. Examples of representative temperature traces have been given in the response to Question 001.416.

REFERENCES

Q001.419-1. M. D. Carelli and D. R. Spencer, "CRBRP Assemblies Hot Channel Factors Preliminary Analysis, "Westinghouse Advanced Reactors Division, Madison, Pa., WARD-D-0050, October 1974.

Q001.419-2. WARD-2171-39, "Verfication of Predicted FFTF Natural Circulation Capability Using SEFOR Steady State Tests," C. R. Adkins, et al, October, 1973.

Q001.419-3. ANL-CT-75-23, "A Study on the Nodal Approximation of the CRBR Core Simulation," D. Saphier, L. W. Kirsch, T. P. Mulcahey, January 1975. (Availability: USERDA Technical Information Center)

7683-144

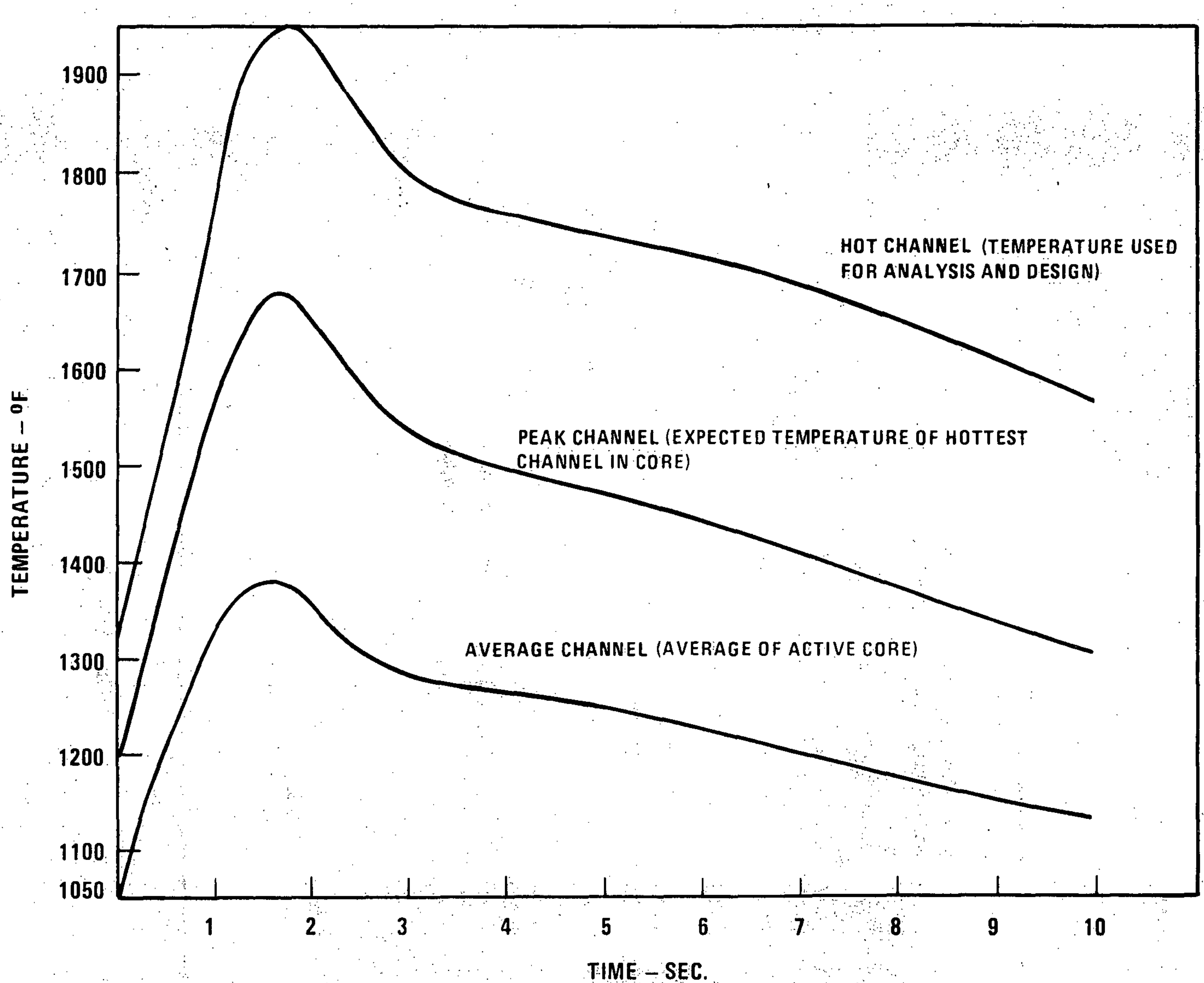


Figure Q001.419-1. Core Exit Coolant Temperatures For Double-Ended Rupture At R.V. Inlet Sleeve As Calculated By The DEMO Code (Single Phase Flow Model)

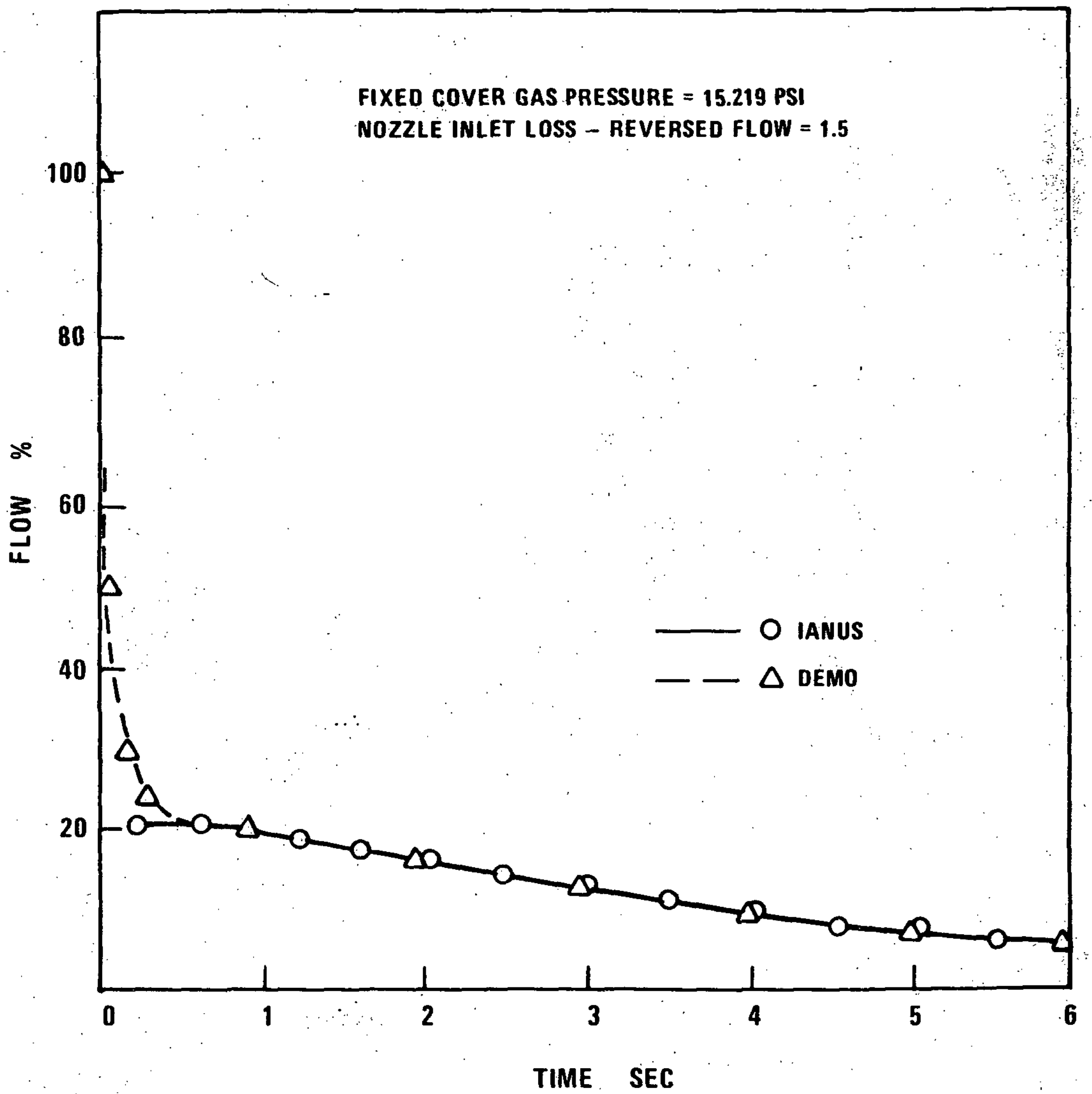


Figure Q001.419-2. Pipe Rupture At Inlet Nozzle Reactor Flow Rate

7683-155

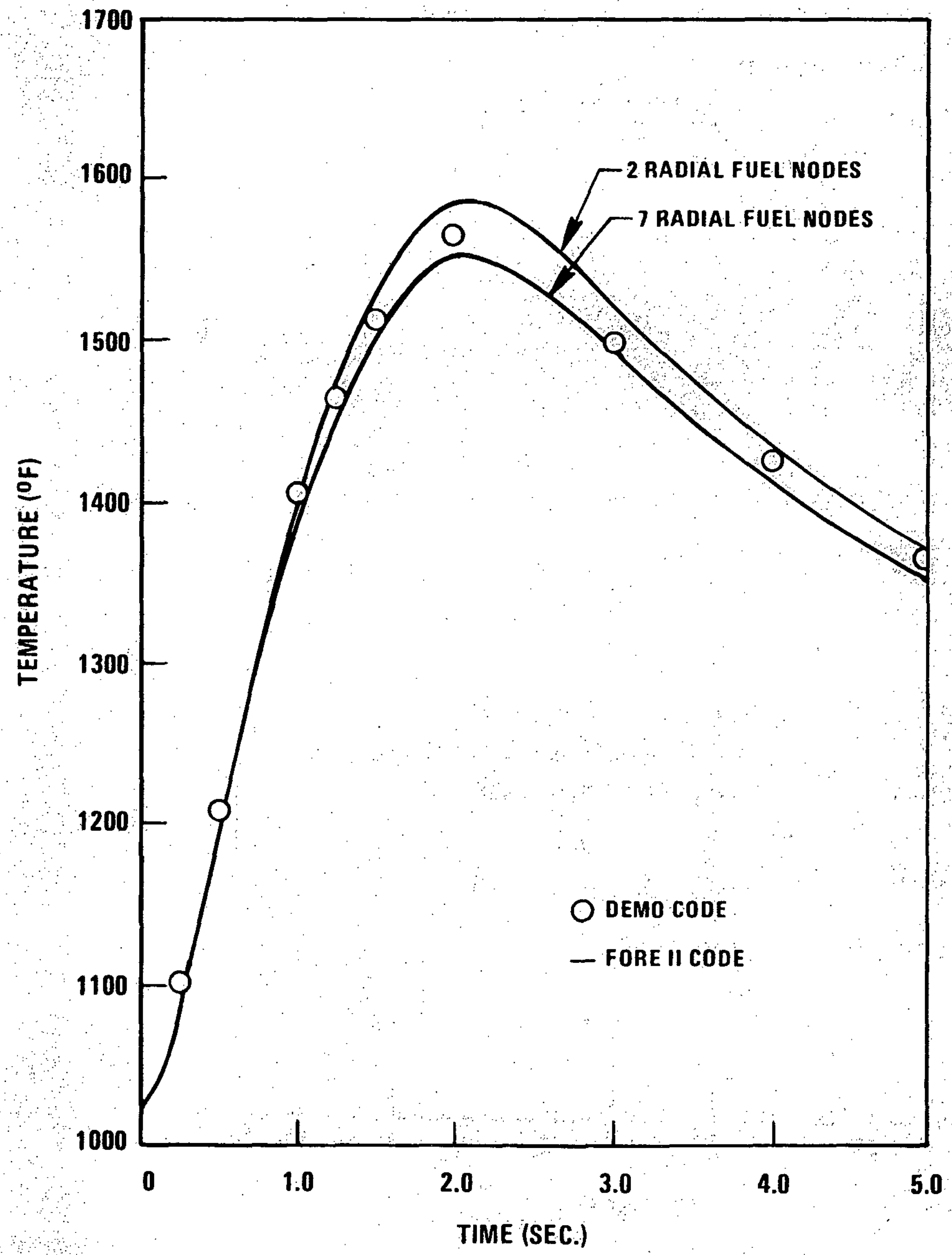


Figure Q001.419-3. Transient Coolant Temperature of Average Channel At Top of Active Core For Pipe Rupture Comparison Study

7683-156

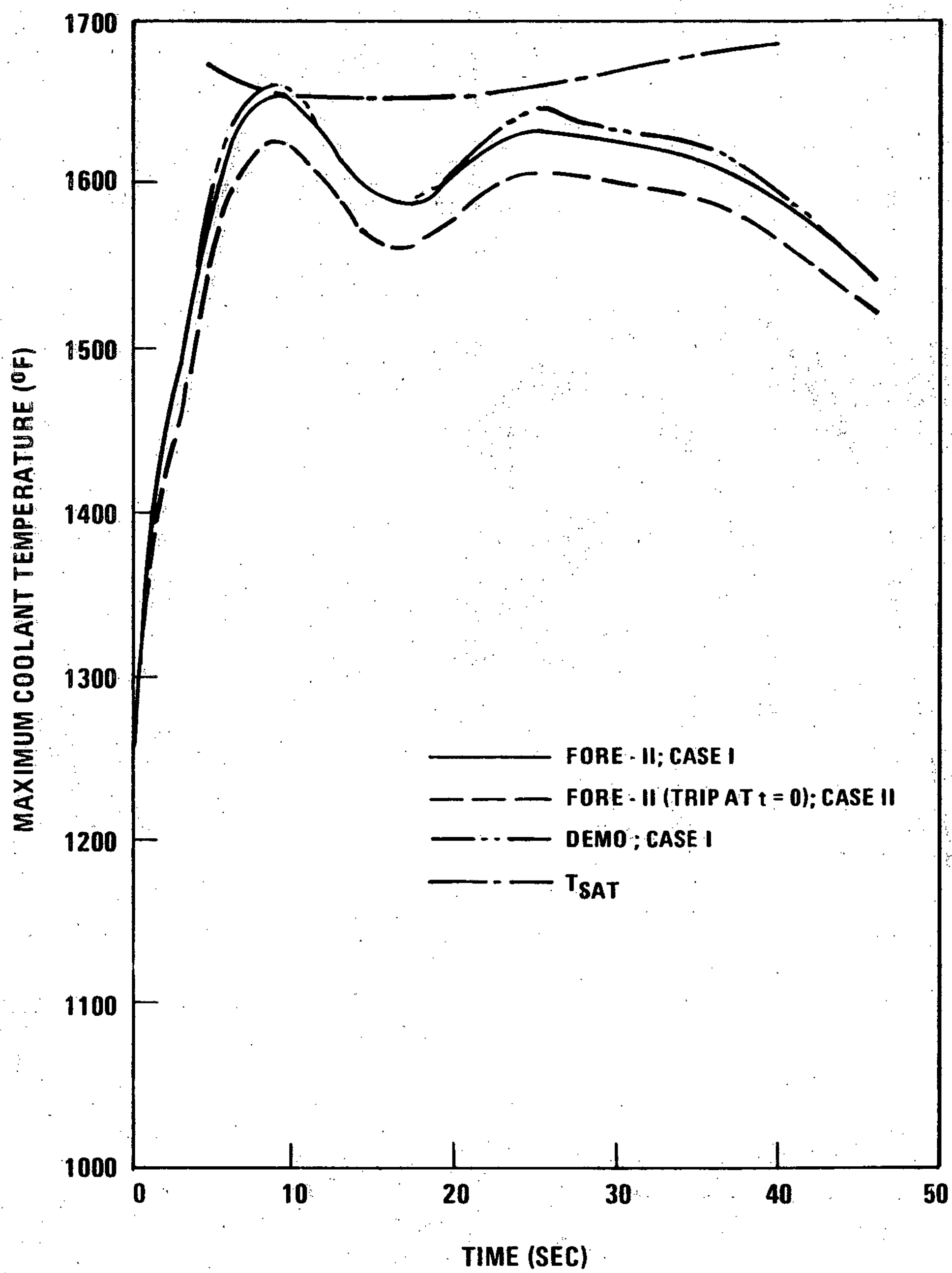


Figure Q001.419-4. Hot Spot Coolant Temperatures For RB/A A-1 During Hypothetical Pipe Rupture Transient

7683-157

Question 001.420 (E.3.1.3.4)

Provide the technical justification for locating the flow restrictor approximately 20 feet above the reactor inlet nozzle. Provide the flow characteristics for the flow restrictor as well as the basis for establishing these characteristics and the estimated uncertainty in these characteristics.

Response:

The flow restrictor is located such as to roughly cut in half the total volume in the pipe/sleeve annulus that has to be filled with sodium in case of a postulated pipe rupture, before the back pressure is generated that stops the break flow. As Figure Q001.420-1 indicates, an annulus with a radial gap of about 3.85 in. or less would be required to limit the amount of sodium egress such that the short-term coolant temperature peak at the core hot channel outlet does not exceed the saturation temperature. Because of access requirements a radial gap of 7.0 in. has been chosen. This increases the annulus volume by a factor of $(19.0^2 - 12.0^2)/(15.85^2 - 12.0^2) = 2.02$ (for pipe and sleeve dimensions see Figure E.3-5). Thus the flow restrictor decreases the available annulus volume down to the required size.

For defining the flow characteristics, the restrictor is modeled as an orifice. Using the current radial gap of 0.5 in. between the pipe and the restrictor, the cross sectional flow area of the orifice is $A_R = 0.267$ ft^2. The pipe/sleeve annulus has a cross sectional flow area of $A_A = 4.73$ ft^2 which results in an area ratio of $A_R/A_A = 0.056$. The pressure drop across the flow restrictor is then calculated as

$$\Delta P = K\,(V^2/2g)\gamma$$

where K is the friction loss coefficient, $V^2/2g$ is the velocity head and $\gamma = \rho g$ is the density of the sodium times the gravitational constant. The friction loss coefficient is calculated (according to the CRANE Handbook of Hydraulics) as

$$K = \left(1/\alpha - \frac{A_R}{A_A}\right)^2$$

with

$$\alpha = 0.63 + 0.37\left(\frac{A_R}{A_A}\right)^3$$

using the above given value of $A_R/A_A = 0.056$ yields $K = 2.3$.

The uncertainty in the calculated value for ΔP is estimated at approximately 10 percent.

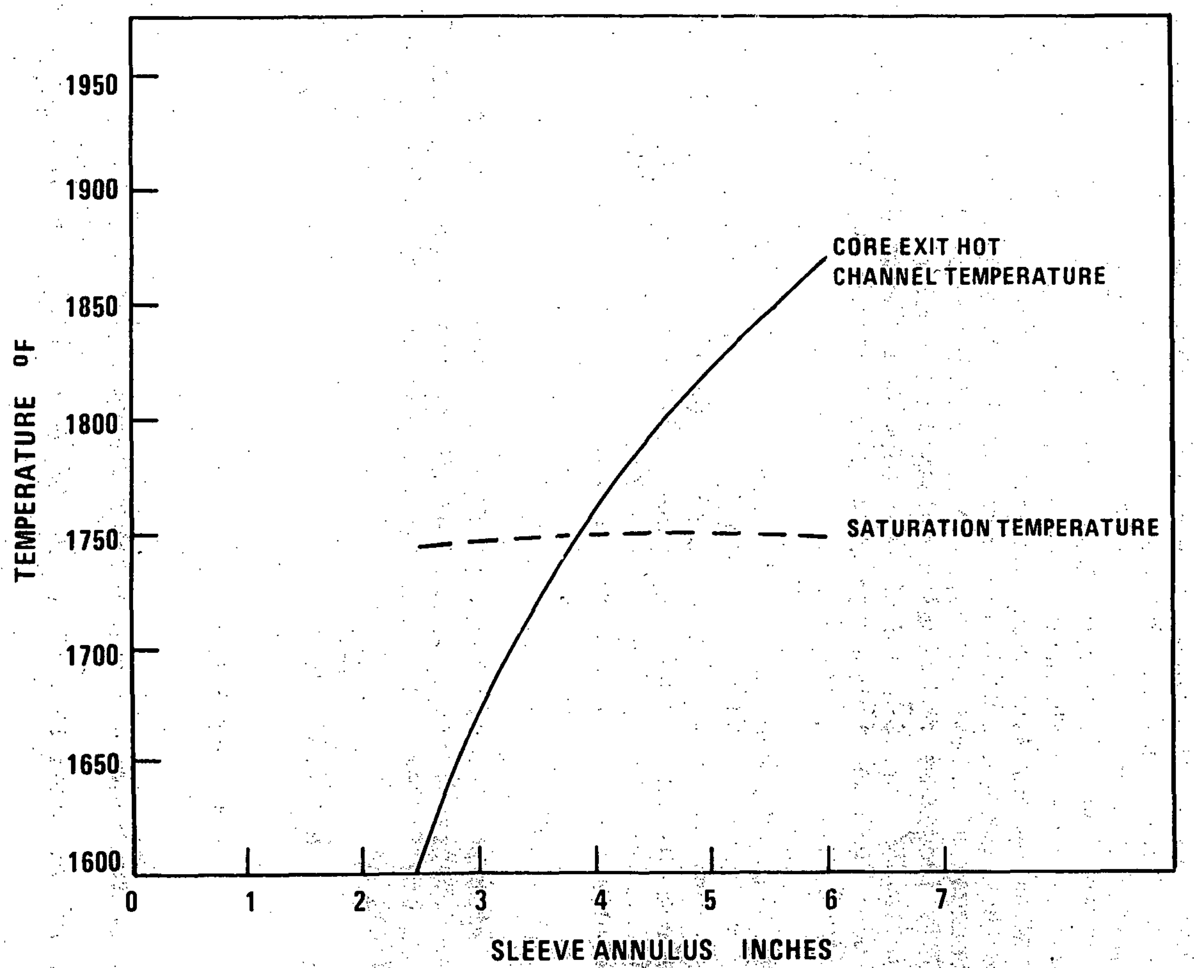


Figure Q001.420-1. Core Exit Hot Channel Temperature vs Size Of Sleeve/Pipe Annulus

Question 001.421 (E3.1.3.4)

Clarify the last paragraph in this section where reference is made to the coolant margin to boiling that is required to satisfy the acceptance criteria.

Response:

The response to this question is provided in the revised Section E3.1.3.4.

Question 001.422 (E.3.1.3.4)

Discuss the structural integrity of the flow restrictor and its expected performance under the load conditions associated with pipe rupture in its proximity as well as at other locations along the downcomer.

Response:

The flow restrictor was included in the design option developed for accommodation of a postulated PHTS cold leg pipe rupture as a design basis event. Based on the belief that NRC concerns with respect to inservice inspection, material surveillance and leak detection can be satisfied, the Project no longer considers the pipe rupture as a potential basis for parallel design efforts. This is consistent with the NRC Staff position presented at the July 9 meeting of the Advisory Committee on Reactor Safeguards. Hence a direct response

27 to this question is not considered appropriate.

Question 001.423 (Q001.275)

In connection with Item 001.275 provide scale drawings showing the details and locations of the pipe and sleeve, support structures and hangers, and flow restrictor.

Response:

New Figure E.3-5A, Pipe Sleeve Interface Control Drawing (Preliminary), provides the details and locations of the Reactor Vessel inlet pipe, flow restrictor and pipe sleeve seismic restraint. Flow restrictor details are shown in Figure E.3-6 of the PSAR, and the pipe sleeve seismic restraint in Figure E.3-7. The support structure and hanger locations, for the Reactor Vessel inlet pipe will remain as per reference design.

Question 001.424 (References)

Provide references 2 and 4 on page E.3-14; Revision 4 of WARD-D-0005, November 1975, and "CRBRP Decay Power Analysis", WARD-D-0090, July 1975.

Response:

Reference 2: Revision 4 of WARD-D-0005 was provided to NRC on March 29, 1976.

25 Reference 4: "CRBRP Decay Power Analysis", WARD-D-0090 has been provided under separate cover.

Question 001.425 (Appendix E)

Although the role of reliability methods in the safety analysis of nuclear reactor systems has been increasing, large inaccuracies as a result of inadequate data base may be expected, especially for first of a kind systems. For example, the Reactor Safety Study (WASH-1400) allowed for an error spread of two orders of magnitude for the failure rates associated with LWR piping systems.

Provide the confidence interval associated with the point estimate for the probability of pipe rupture in the primary heat transport system. Show in sufficient detail the analysis used in deriving this confidence limit.

Response:

A fairly detailed structural reliability assessment of the primary cold leg piping was provided to NRC by WARD-D-0127, submitted in December 1975. (Reference 2 of PSAR Section 1.6) Those results show a tolerance of two orders of magnitude due to uncertainties without violating the piping integrity reliability goal.

Question 001.426 (Appendix E)

In a recent evaluation of the integrity of LMFBR primary piping, Chow et al.* concluded that the linear elastic fracture methodology (as proposed in Appendix C of the PSAR) is rather limited in its ability to describe crack growth at elevated temperatures in LMFBR primary piping.

Provide the justification for the use of linear elastic fracture methodology in the structural reliability assessment of the primary piping.

*Reference: "Integrity of LMFBR Piping: A Preliminary Evaluation" J.G.Y. Chow et.al., BNL/FRS-74-2, September, 1974.

Response:

The normal operating temperature of the cold leg is around 750°F which is well below the creep range for 304 stainless steel. The application of linear elastic fracture mechanics methods to crack growth calculation is well established if creep is not a factor (see Reference Q001.426-1. As an example, to verify that the fatigue-crack growth rate is an unique function of stress intensity factor; Figure Q001.426-1 presents a compilation of fatigue-crack growth behavior for 304 SS at room temperature as obtained from the ten specimen types shown in Figures Q001.425-2 and 3. Figures Q001.426-1, 2 and 3 are reprints from Reference Q001.426-1. Further substantiation is found in Reference Q001.426-2. In addition, James (Reference Q001.426-3) concludes that creep is a second order effect on the crack growth of 304 SS at 1000°F. Since this temperature is the maximum imposed by any duty cycle event (see Reference Q001.426-4) on the cold leg piping, linear elastic fracture mechanics application to crack growth evaluations is applicable. Moreover, the strain-hardening properties of the piping material at the cold leg temperatures are such that, where stresses exceed yield, shakedown to elastic behavior occurs after a very few cycles (see Section 4.4 of Reference Q001.426-3 and Reference Q001.426-5).

The piping integrity assessment of the hot leg piping has not been documented. The applicability of linear elastic fracture mechanics for the hot leg is under study and will be addressed in the hot leg piping assessment which will be provided as a supplement to Reference 2 of PSAR Section 1.6.

References:

Q001.426-1 L. A. James, "Fatigue-Crack Propagation in Austenitic Stainless Steels," HEDL-SA-1051, Hanford Engineering Development Laboratory, Richland, Wash., January 1976.

Q001.426-2 J. A. Begley and A. A. Sheinker, "Crack Propagation Testing for LMFBR Piping - Phase I Final Report," WARD-HT-3045-17, Westinghouse Advanced Reactors Division, Madison, Pa. 15663 (in publication).

Q001.426-3 L. A. James, "Some Questions Regarding the Interaction of Creep and Fatigue," ASME Paper 75-WA/Mat-6.

Q001.426-4 "Primary Pipe Integrity Status Report," WARD-D-0127, Westinghouse Advanced Reactors Division, Madison, Pa., December 1975.

Q001.426-5 "Criteria of ASME Boiler and Pressure Vessel Code For Design by Analysis in Sections III and VIII, Division 2, "American Society of Mechanical Engineers, 1969.

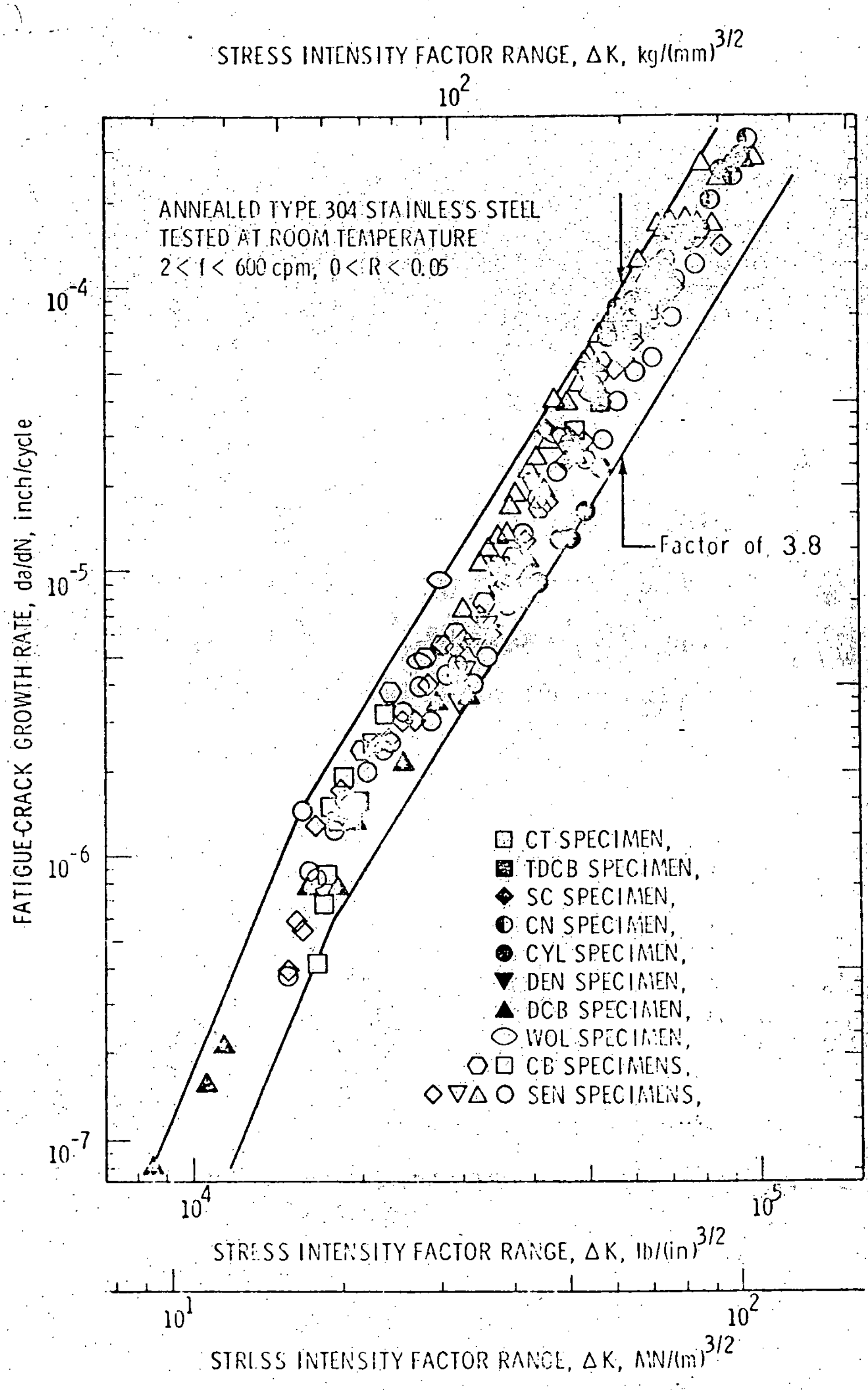
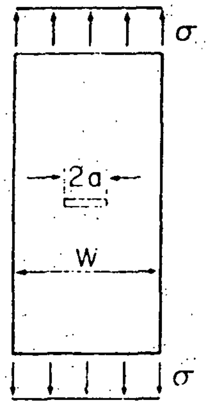


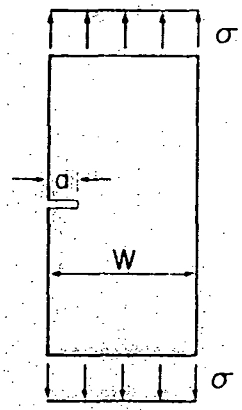
Figure 1. Fatigue-crack growth behavior of Type 304 at room temperature as determined from ten different specimen designs. See Fig. 2 for specimens used.

Figure Q001.426-1 Reprinted from Reference Q001.426-1



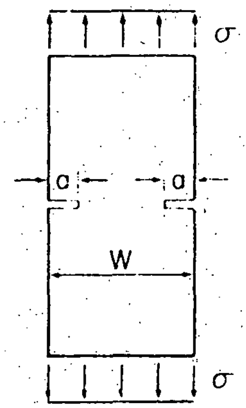
CN
CENTER-NOTCHED

$$K=\sigma\sqrt{a}\left[1.77+0.277\left(\frac{2a}{W}\right)-0.51\left(\frac{2a}{W}\right)^2+2.7\left(\frac{2a}{W}\right)^3\right]$$



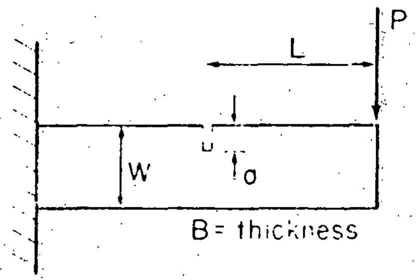
SEN
SINGLE-EDGE-NOTCHED

$$K=\sigma\sqrt{a}\left[1.99-0.41\left(\frac{a}{W}\right)+18.7\left(\frac{a}{W}\right)^2-38.48\left(\frac{a}{W}\right)^3+53.85\left(\frac{a}{W}\right)^4\right]$$



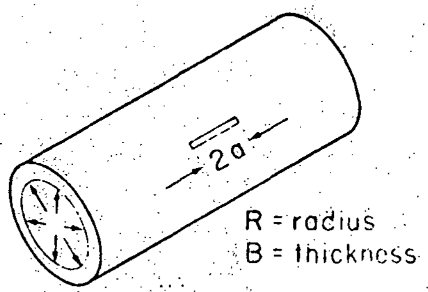
DEN
DOUBLE-EDGE-NOTCHED

$$K=\sigma\sqrt{a}\left[1.98+0.36\left(\frac{2a}{W}\right)-2.12\left(\frac{2a}{W}\right)^2+3.42\left(\frac{2a}{W}\right)^3\right]$$



CB
CANTILEVER-BEND

$$K=\frac{6PL\sqrt{a}}{BW^2}\left[1.99-2.47\left(\frac{a}{W}\right)+12.97\left(\frac{a}{W}\right)^2-23.17\left(\frac{a}{W}\right)^3+24.8\left(\frac{a}{W}\right)^4\right]$$

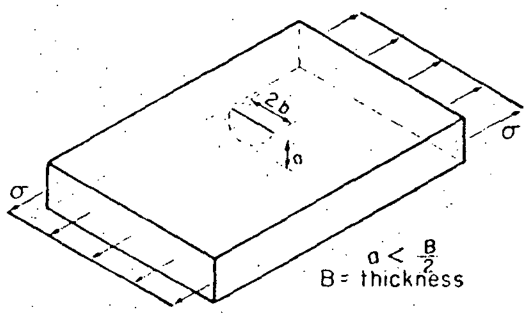


CYL
INTERNALLY-PRESSURIZED CYLINDER
(THRU-WALL AXIAL CRACK)

$$K=\sigma\sqrt{a}\;\; f\left(\frac{a}{\sqrt{RB}}\right)$$

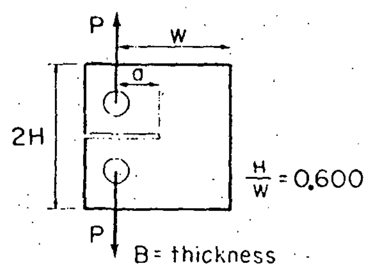
Figure 2a. Specimens used to obtain the data shown in Figure 1.

Figure Q001.426-2 Reprinted from Reference Q001.426-1



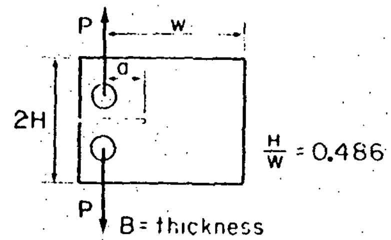
SC
SURFACE-CRACKED

$$K = \frac{\sigma\sqrt{\pi a}}{\int_0^{\pi/2}\left[1-\left(\frac{b^2-a^2}{b^2}\right)\sin^2\theta\right]^{1/2}d\theta}$$



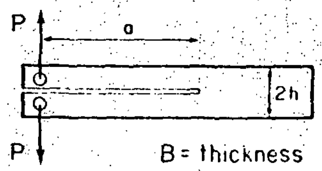
CT
A.S.T.M. COMPACT TENSION

$$K = \frac{P}{B\sqrt{W}}\left[29.6\left(\frac{a}{W}\right)^{0.5} - 185.5\left(\frac{a}{W}\right)^{1.5} + 655.7\left(\frac{a}{W}\right)^{2.5} - 1017.0\left(\frac{a}{W}\right)^{3.5} + 638.9\left(\frac{a}{W}\right)^{4.5}\right]$$



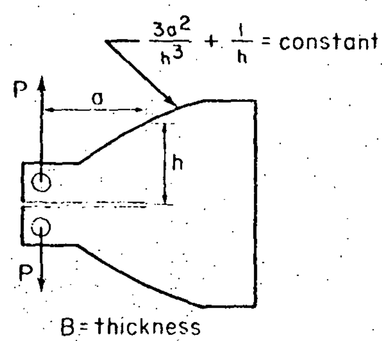
WOL
WEDGE-OPENING-LOADING

$$K = \frac{P}{B\sqrt{a}}\left[30.96\left(\frac{a}{W}\right) - 195.8\left(\frac{a}{W}\right)^2 + 730.6\left(\frac{a}{W}\right)^3 - 1186.3\left(\frac{a}{W}\right)^4 + 754.6\left(\frac{a}{W}\right)^5\right]$$



DCB
DOUBLE-CANTILEVER BEAM

$$K = \frac{P}{B}\sqrt{4\left[\frac{3a^2}{h^3} + \frac{1}{h}\right]}$$



TDCB
TAPERED DOUBLE-CANTILEVER BEAM

$$K = \frac{P}{B}\sqrt{4\left[\frac{3a^2}{h^3} + \frac{1}{h}\right]} = \text{constant}$$

Figure 2b. Specimens used to obtain the data shown in Figure 1.

Figure Q001.426-3 Reprinted from Reference Q001.426-1

Question 001.427 (Appendix E)

The Parallel Design (pipe sleeve) for the primary piping may have some disadvantages which should be considered in a reliability assessment, e.g., a lack of inspection access to the welding of the primary piping at the inlet nozzle.

Provide a detailed reliability assessment, parallel to the one carried out for the Reference Design (described on page E.4-4 of Appendix E), for the alternate, pipe sleeve, design. This should include a comprehensive failure-modes-and-effects analysis associated with stress loading of the pipe sleeve as a result of a double-ended pipe rupture. In particular, provide analyses which establish the survivability of the pipe sleeve following a double-ended rupture of the primary pipe at the inlet nozzle. Include a description and labeled drawings or diagrams of the primary piping support structures with the pipe sleeve present.

Response:

The pipe sleeve design will be such that adequate inspectability will be provided, as delineated in Appendix E. Relevant information can be found in Sections E.3.1.2.1 and E.3.1.2.3.

The structural design of the pipe sleeve will employ well established deterministic analysis and design methods. Under the design basis pipe break conditions, the design of the pipe sleeve will assure its structural integrity necessary to perform its specified safety function. Therefore, we do not plan to perform a "detailed reliability assessment for the pipe sleeve design". However, if the pipe sleeve is implemented in the design, an FMEA will be performed to assure the pipe sleeve doesn't introduce any unrecognized failure modes which might contribute to loss of coolable geometry.

Details of the piping support structure associated with the pipe sleeve are shown in Figure E.3-7. Beyond the top of the reactor guard vessel, the piping insulation performs the sleeve function and no changes in the piping support structures from the Reference Design are anticipated.

Since inspection capabilities are not affected by the presence of the sleeve, and no changes are made in the piping support structure, it is concluded that the pipe sleeve does not affect the assessment of decay heat removal reliability, and additional reliability assessment for the pipe sleeve design is not required.

Question 001.428 (Appendix E)

The response of cell liners has been previously questioned in item 130.37 and in the request for topical information, item 15, (letter dated October 6, 1975). These concerns are relevant in connection with pipe rupture due to the potential for water evolution from concrete in situations involving either partially or fully lined cells. The potential for hydrogen generation from this water, and any subsequent procedures for venting containment should be addressed.

Response:

The Reactor Cavity and the PHTS cells are fully lined cells. The response to Question 001.210 discusses prevention of liner failure, venting of steam pressure, containment purging and hydrogen evolution as associated with Reference Design sodium spills. The information contained in the response to Question 001.210 is also applicable to Appendix E sodium spills.

Question 001.429 (E3.1.2.1)

Provide the information requested in Question 120.38 for the inner reflective sheathing for the piping thermal insulation which is to perform the function of a sleeve with a 1.5" annulus for pipe rupture accommodation. Describe the analytical or experimental methods and design criteria to be used in designing the insulation shielding and inner sheath supports to withstand the loads associated with a possible pipe rupture.

Response:

The use of thermal insulation sheathing as a piping sleeve was included in the design option developed for accommodation of a postulated PHTS cold leg rupture as a design basis event. Based on the belief that NRC concerns with respect to inservice, inspection, material surveillance and leak detection can be satisfied, the Project no longer considers the pipe rupture as a potential basis for parallel design efforts. This is consistent with the NRC Staff position presented at the July 9 meeting of the Advisory Committee on Reactor Safeguards. Hence a direct response to this question is not considered
27 | appropriate.

Question 001.430 (E.3.1.1 Green)

The parallel design study for pipe rupture proposes addition of flow restrictors and sleeves to the reactor inlet piping up to the check valve to protect against reduced core flow in the event of a pipe rupture in this region. Pipe integrity studies will attempt to show pipe rupture not to be a credible event in either the hot leg or the cold leg without prior leakage and that there will be adequate time for shutdown. Leak detection with suitable sensitivity and response characteristics is necessary to sustain the logic of this argument. Discuss how your plans would change if this position could only be supported for the cold leg (400°F - 750°F) piping which operates in the elastic regime. Current efforts appear to be primarily directed to defects in the cold leg piping and conversely leak detection development appears to be oriented towards detecting hot leg sodium leaks. Discuss the change in leak detection sensitivity between hot and cold leg piping; describe and discuss the R&D program to develop the leak detection system for both cases.

Response:

Additional information in response to the first part of the question concerning the hot leg piping design is provided in amended Section 1.5.2.1.5.

Revised Section E.3.1.2.4 provides the leak detection information requested.

Question 001.431 (E3.2 Yellow)

The principal thrust of the proposed reliability studies seem to be directed to show the adequacy of the QA program in eliminating large defects and that those defects that escape detection could not generate a self-propagating crack in the stress field in which the system operates (i.e., leak before break). The scope should be broadened to include other failure initiating mechanisms such as design errors, construction mistakes, QA errors, and deleterious environmental effects. Relate the proposed reliability studies to actual LWR experience rather than to the limited LMFBR experience.

Response:

A rather large initial flaw size was selected as a starting point in the analysis provided to NRC in WARD-D-0127. The size of the flaw and the basis for its selection are described in that report. The flaw selection, as discussed in WARD-D-0127 is intended to reflect the risk of construction mistakes and QA errors. It is assumed that the undetected flaw is sharp edged, oriented for maximum growth, and placed at the point of highest stress. These assumptions provide margin against design errors and construction mistakes which might result in higher stresses than expected.

WARD-D-0127 has shown that reactor grade sodium does not produce any deleterious effects. Only the reaction products of leaking sodium could produce significant deleterious environmental effects. These would be mitigated by rapid detection of small leaks as discussed in WARD-D-0127.

Piping integrity analyses of the LWR's are clearly of general technical interest. However, such differences in geometry as much thicker walls and such differences in piping failure effects as the significantly greater stored energy in LWR piping make the comparisons of limited usefulness to CRBRP. Consequently, no extensive engineering effort on LWR piping comparisons is judged to be warranted. However a review of LWR operating experience is currently underway. The reported LWR abnormal occurences are being screened to determine which are relatable to CRBRP. The ongoing reliability studies will include an assessment of the protection provided by the CRBRP reference design against the appropriate LWR observed abnormal occurrences.

Question 001.432 (E4.1 Yellow)

Several considerations are related to sodium leakage other than pipe rupture and the installation of a sleeve in the reactor cold leg between the reactor and the check valve. Among these are the cell design pressure, venting and placement of hot and cold cell liners. Since a spectrum of leaks could be involved giving rise to a series of design options, provide further clarification by discussing leak rate vs cell pressure, venting and cell liner options.

Response:

Preliminary assessments of the PHTS cell maximum pressure versus sodium leak rate are provided in response to Q040.4. These preliminary assessments were based upon the design basis leak approach transmitted to NRC in the Information in Advance of CRBRP Cell Liner Design Meeting on June 8, 1976.

In addition to the evaluation of the design basis leak, the cell liner system and structural design has been evaluated for system inventory spills to determine margin. The analytical results are being confirmed by a sodium (evaluation) dump experiment at HEDL schedule to be made in mid-year 1977.

The present PHTS cell and reactor cavity design does not differentiate between hot and cold liners as described in the previous referenced advanced cell liner information. Liners are provided with insulation and a gap for venting steam/vapor from behind the liner.

The structure of the PHTS cells and the reactor cavity has been evaluated and can withstand transient pressures of 30 and 35 psig, respectively, and venting between cells for pressure relief is presently not anticipated as being necessary. Venting between cells would be undesirable because of the potential of contamination of larger areas than necessary.

Leak detection means are being provided to detect leaks as small as 100 gm/hr in 250 hours. Liners are being designed to accommodate the design basis leak. To insure additional margin, the cell liners and structures are being evaluated for system characteristic sodium spills larger than the design basis leak.

Question 001.433 (15.A.3.3.1 Yellow)

In the analysis of the EVCC, the initial temperature is taken as 1075°F, and the elevated temperature due to decay heat is limited to 1200°F. The flow blockage analysis in Figure F 6.4-5 shows however that the sodium temperature in the vessel can rise as high as 1400°F prior to melt-through to the EVCC. Discuss the effect this would have on the partition factors for the fission products dispersed in the sodium, and consequently, on the RC source term (Table 15.A.3-4). Justify extending the method of partition fractions, as used by Castleman, if the sodium temperature is calculated to rise beyond 1200°F; note that Cs and Rb boil below 1300°F.

Response:

In Amendment 24 to the PSAR, the Project withdrew the Parallel Design from further consideration by the NRC staff. This question requests additional information relative to analyses conducted in support of the Parallel Design. Accordingly, the question is no longer directly applicable. The considerations associated with developing the source term for the TMBDB analyses are discussed in Section 4 of CRBRP-3, Volume 2 (Reference 10b of PSAR Section 1.6).

Question 001.434 (6.2.5.2)

You state in Section 6.2.5.2.1 that the heat removal system for the Reactor Cavity shall be designed to maintain certain temperature during normal operating conditions, and that its operation is not required following a CDA. Provide the design criteria for this system and discuss the consequences of its failure during normal operation.

Response:

The response to this question is provided in revised PSAR Section 3A.1.3.

Question 001.435: (F.3.2)

Assuming the accident sequence in F3.2.1.1 but with pump trip at the expected trip conditions, what differences in the scenario would be expected?

Response:* 60

The accident sequence postulated in this question should not be considered as an HCDA initiator for the reasons discussed below.

Table Q001.435-1 shows the five trip functions and trip levels that apply to the startup situation. For the assumed case of a continuous control rod withdrawal, the first trip would be at $\sim$20% power due to the Primary Shutdown System (PSDS) Flux-Delayed Flux trip function. The backup functions are the Startup Nuclear (SSDS) at $\sim$25-30%, the Flux-Total Flow (SSDS) at $\sim$60%, the Flux - $\sqrt{\text{Pressure}}$ (PSDS) at $\sim$60%, and the High Flux (PSDS) at 115%.

One may postulate that the SDSs fail either electrically only or mechanically only. The postulated sequence of failures can be discussed more fully by use of Figure Q001.435-1 which reflects the startup condition. 60

Path #1 on this figure is a postulated case of two electrical failures. These would leave the pumps running and the rod continuing to withdraw. This is the case discussed in Section 4.3 of Reference 10a, PSAR Section 1.6. 60

Path #2 is the case postulated by the question and would require all of the following failures:

- Failure of the CRDM pulser or operator error
- Failure of the rod block interlock
- Simultaneous common mode failure of at least 2 of the 3 independent electrical trains of the primary shutdown system
- Simultaneous common mode mechanical failure of at least 2 of 4 secondary rods.

Thus, only by postulating two unrelated (one electrical and one mechanical) common mode failures can the scenario postulated by question 001.435 occur. One common mode failure must involve the primary electrical system and cannot involve the secondary electrical system. The second common mode failure must involve the secondary mechanical system, not involve the primary mechanical system, and prevent at least two of the secondary rods from being inserted. Therefore, this sequence is less probable than the postulated common mode failure of both electrical systems and should not be considered as an initiator.

*Note that Appendix F has been withdrawn. However, the accident sequence referenced in the question related to a control assembly withdrawal at startup with shutdown system failure. 60

In paths 3 and 4 it is postulated that there are common mode mechanical failures of 14 of the 15 primary rods and, obviously, no mechanical failure of the rod postulated to be continuously withdrawing. The overall probability of path 3 is significantly smaller than paths 1 or 2 because of the large number of rods which must experience a common mode failure and because of the mechanical diversity between the primary and secondary systems. The overall probability of path 4 appears to be even smaller than that of path 3.

Summarizing the four conceptual paths discussed above, it may first be noted that path #1 does not result in a pump coastdown because no rod mechanical failures are involved. In paths 3 and 4, a mechanical failure of the PSDS would result in a trip of the primary coolant pumps and interruption of electrical power to the control rods stopping the withdrawal. Since the trip would occur at about 30% power, the reactor power would remain at that level. Only path #2 shows a sequence of events consisting of continuous power increase plus trip of the primary pumps.

If the event postulated in question 001.435 could occur, the coastdown of the flow (which would be initiated at about 30 seconds) would result in hot channel coolant boiling in about 35 seconds. At that time the reactor power would be about 50% of full power and the flow would be about 20% of full flow. The coolant boiling would result in cladding failures and the continued heating would result in fuel melting. This would eventually result in a slow core meltdown characteristic of the Transition Phase analyses presented in Section 4.2.3 of Reference 10a, PSAR Section 1.6. 60

TABLE Q001.435-1

SUMMARY OF PPS TRIPS FOR STARTUP POWER RANGE

Trip Function	Shutdown System	Approximate Trip Level
Flux-Delayed Flux	Primary	20%
Startup Nuclear	Secondary	25 - 30%
Flux-Total Flow	Secondary	60%
Flux - $\sqrt{\text{Pressure}}$	Primary	60%
High Flux	Primary	115%

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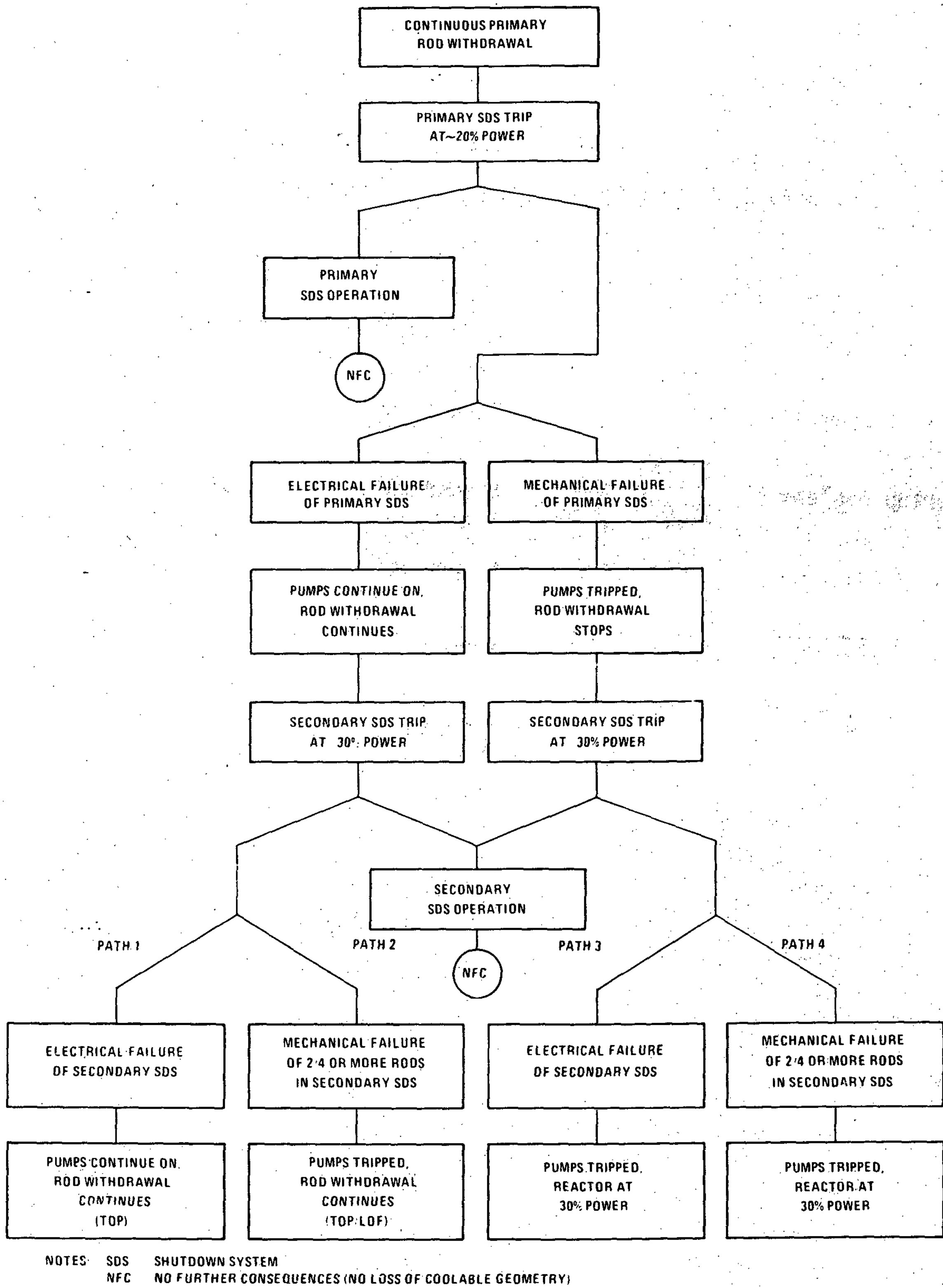

Figure Q001.435-1. Accident Progression Diagram Assuming Continuous Control Rod Withdrawal At Startup And Failure Of Both Shutdown Systems

7683-153

Question 001.436 (F3.2)

Indicate if ramp rates much greater than (such as 19¢/sec) or much less than 2.4¢/sec are possible during startup. If so, what differences in the F3.2.1.1 scenario would be expected?

Response:

The CRBRP Project has consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. The response to this question is now found in Section 3.3.2.2 of Reference 10a, PSAR Section 1.6.

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Question 001.437 (F3.2)

Discuss the reason for the preliminary conclusion that the event in F3.2.1.3, Seismic Reactivity Insertion-Operation Basis Earthquake (OBE) With Shutdown System Failure, will be similar in consequence to the LOF event of F3.2.2.1. Is there a greater possibility for a hydrodynamic disassembly as opposed to the cited LOF events? Provide the schedule for performing the detailed analysis of this event.

Response:

The CRBRP Project has consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. The response to this question is now found in Section 4.4.2.2.3 of Reference 10a and Section 8 of Reference 15, PSAR Section 1.6. 60

Question 001.438 (F3.2)

It is not obvious that a common mode failure(s) involving both primary and secondary systems has an overall probability "significantly smaller" than failure of both systems and failure of the pumps to trip on demand. Provide the justification to support this view. Indicate if a flow transient from an overpower condition would be similar to (a) the LOF at normal power, (b) the scenario in F3.2.1.3.

Response:*

The relative likelihood of various failure sequences is discussed in the response to Q001.435.

The consequences of flow transients initiated from an overpower condition are provided in Reference 10a, PSAR Section 1.6, Table 4-4, cases L33 through L40. 60

*Note that Appendix F has been withdrawn. However, the scenarios referenced in the question related to:

a) Loss of Off-Site Electrical Power with Shutdown System Failure

b) Seismic Reactivity Insertion (OBE) with Shutdown System Failure 60

Question 001.439 (F3.2)

Specify the approximate length of time that the reactor can operate at 115% power before component design stresses are exceeded or fuel failure is expected.

Response:

The CRBRP could operate at 115% for several days before any significant consequences would result from the overpower condition. The long term consequences of operation at 115% power will be discussed below. However, it is important to first note the present design is such that the plant will be tripped at 115% power by the PPS and that the plant will be manually tripped within 5 minutes at an overpower condition less than 15%.

An analysis of the consequences of operation at 115% power was performed to ascertain whether any short term safety consequences would result. First, a prediction was made of the final steady state flows and temperatures for the system. This was done by a simple extrapolation from the U-2B transient (an Upset Event, uncontrolled rod withdrawal with trip). The plant is designed for sixteen U-2b transients in which it is assumed that the plant is tripped after 5 minutes of operation at 115% power. During the period preceeding the reactor trip, it was conservatively assumed that the PHTS and IHTS sodium flows remained constant at their initial steady state values during the overpower condition. The feedwater flow was allowed to be increased by the automatic control system. The approximate final temperatures and flows are given in Table Q001.439-1. It is seen from the table that the largest temperature increase would be only 65°F and would occur in the PHTS hot leg. It is also noteworthy that the increase in the PHTS cold leg is only 30°F and that this results in a temperature of 780°F which is well below the creep regime for the material.

A scoping calculation was performed to determine the period of time the CRBR could be maintained at 15% overpower before fuel and radial blanket rod cladding design limits were exceeded. For the assemblies considered, this calculation showed that this time period is minimum when the overpower is applied at end of design life, which is 1.5 equilibrium cycles for the fuel rods and 6 equilibrium cycles for the radial blanket rods.

The conditions assumed for these calculations, the fuel and radial blanket assemblies considered, and the principal results of the calculations are summarized in Table Q001.439-2. The design limit utilized in this calculation is the cladding cumulative damage function (CDF) limit of 1.0. This design limit and the method for calculating cladding CDF is fully discussed in Section 15.1.2 .

To determine the allowable overpower operating period, calculations for the fuel and radial blanket rod cladding CDF versus time up to the end of design life were repeated. The upset transients were included in this part of the calculation, but the emergency transient was not. At the end of design life the 15% overpower environments were imposed on the rods and the cladding CDF versus time calculations were continued until the time for the cladding CDF to reach 1.0 was determined. No additional transients were imposed during the 15% overpower period after end of design life. Effect of the 15% overpower environment on the cladding CDF was calculated assuming hot spot cladding temperatures at a 3σ level of confidence and thermal-hydraulic design conditions.

These results show that the fuel rods can be maintained at 15% overpower for approximately 12 days past end of design life before the CDF limit is exceeded. The radial blanket rods can be maintained for approximately 4 days past end of design life at 15% overpower before this limit is exceeded. These time values are very conservative, minimum quantities since 3σ environments were used in these calculations. If the 15% overpower environments would have been assumed at the 2σ level of confidence, the times to exceed cladding limits would have been considerably larger.

Even if a few radial blanket or fuel assembly rods were to fail, the consequences would be acceptable as shown in Section 15.4.

Reactor structural components would also not undergo any significant adverse effects. Time at 115% power, in addition to the overpower phase of the U-2b events for which the reactor structures are designed, would not cause significant additional creep damage and would be acceptable for a time duration in the order of weeks.

Operation at 115% power would also be acceptable for times up to the order of years for the heat transport system. As is shown in Table Q001.439-1, the temperature increases are 65°F or less. The PHTS and IHTS cold legs stay well below the creep temperature. Since the PHTS and IHTS pumps remain at constant speed, all of the primary or equilibrium type of stresses would remain essentially the same. There would be a small decrease in the allowable primary stresses. Stated more specifically at the beginning of plant life, if the 316SS hot leg components were operating at the maximum allowable primary stress S_t which is based on the full lifetime of the plant (approximately $3x10^5$ hrs.) and if the higher temperature occurred, the plant could be operated for 3.4 years before the code design allowables for the higher temperature would be exceeded. The 316SS intermediate hot leg components could operate at 1010°F for 5.7 years before the Code design allowable stresses would be exceeded. For the 2-1/4 Cr - 1 Mo steam generator operating at 1010°F, the time would be 3.4 years before the design allowable stresses would be exceeded. On the other hand, at the end of design life, there is no pre-defined primary stress margin over and above the design duty cycle load history so that no definite time to reach the design limit greater than zero can be assured.

The secondary stresses in the piping would increase slightly due to increases in thermal constraint stresses from the higher temperatures. This could lead to increased ratchetting. The secondary stresses will be limited so that strain limits (0.5%) at welds which cannot be exceeded over the lifetim of the plant. At the beginning of plant life the 316SS hot leg components operating at the 1080°F with a 10% increase in the previous operating stress would produce the design strain of 0.5% after 1.2 years of operation at the higher temperature. For the 2 1/4 Cr-1Mo and 316SS components at 1010°, the design strain would not be produced for 2.3 years at the higher temperatures and stresses. As for the primary stresses, at the end of life there is no pre-defined strain margin over and above the design duty cycle load history, so that no definite time greater than zero can be assured.

The additional creep and fatigue damage that would occur at the slightly elevated temperatures would be very minimal because the fatigue curve for 316SS is the same for the temperature range from 1000°F to 1200°F. In addition, from stress rupture considerations and using the logic above for primary stresses and secondary strains, at the beginning of plant life, hot leg components are predicted to operate from 2.3 to 7 years at the higher temperatures and similarly no definite time greater than zero can be assured at the end of life. Hence, the acceptable lengths of time that the reactor can operate at 115% power could be anywhere from zero to about 1.2 years before exceeding design limits as explained above.

The acceptable lengths of time that the HTS can operate at 115% power could be up to about 1.2 years before exceeding the design limits as explained above. In addition the heat exchangers, steam generators, piping and other major components would have steady state stresses which are usually low because the extreme and/or limiting stresses result from the many thermal transients for which those components are designed including the sixteen U-2b events. Therefore, the stresses and strains used above to determine the acceptable operating times at elevated temperatures are very conservative.

In summary, the postulated transient in which 115% power is achieved is very improbable, but would be acceptable in terms of thermal stresses and fuel failures for periods at least of the order of days for the reactor structures and for periods of the order of weeks to years in the HTS.

TABLE Q001.439-1

APPROXIMATE NSSS CONDITIONS AT POSTULATED
115% POWER

Parameters	975 MWT Initial Conditions *	Final Conditions
Reactor power	100%	115%
Feedwater flow	100%	112%
PHTS hot leg temperature	1015°F	1080°F
PHTS cold leg temperature	750°F	780°F
IHTS hot leg temperature	950°F	1010°F
IHTS cold leg temperature	671°F	680°F

*Thermal Hydraulic design conditions with allowance for instrument error

TABLE Q001.439-2

MINIMUM OPERATING TIME AT 115% POWER
BEFORE CLADDING DESIGN LIMITS ARE EXCEEDED

Rod Type and Assembly (see Figure 4.4-5 in PSAR)	No. of Days Past End-of-Life at 115% Power to Attain Cladding CDF = 1.0
Hot Fuel Rod of Assembly 6	12 days
Hot Radial Blanket Rod of Assembly A	4 days

Notes:

(1) Operating conditions during rod design life:

Steady state - 2σ plant expected
Transient - 3σ T&H design

(2) Operating conditions during 115% power: 3σ T&H design

Question 001.440 (F3.2)

It would appear that the events in F3.2.2.2 through F3.2.2.6 all result in substantially higher bulk sodium temperatures at the onset of hot channel boiling or clad failure than the LOF event in F3.2.2.1. Provide the results of analyses of the event F3.2.2.6 and one other of these events, assuming that no operator action is taken to prevent core disruption. Summarize the differences between these events and the LOF event as regards the general accident progression, the likelihood of moderate to large energetics, and the post-accident heat removal requirements. Provide the reactor coolant temperatures and the coolant saturation temperatures in these cases as a function of time. Also, include a discussion of the effect of the estimated uncertainties in the DEMO Code results on the conclusions.

Response:* 60

The requested information is provided in References 001.440-1 and 2.

Loss of One Heat Transport Loop

The results in Reference Q001.440-1 show that the loss of one heat transport loop with postulated failure of both reactor shutdown systems leads through a 10 minute transition to a new steady state with acceptable increased plant temperatures ($\leq 1150^{o}F$) and reduced reactor power. No operator action is assumed during the 10 minute transition

Loss of Feedwater Flow

The analysis reported in Reference Q001.440-2 indicates that the worst anticipated reduction in feedwater flow coincident with a postulated failure of both reactor shutdown systems is either:

(a) a loss of one out of three feedpumps with failure of the standby pump to start (each pump is capable of delivering 50 percent of full flow); or

(b) a total loss of condensate flow caused by inadvertent closure of the condensate flow control valve.

The results show that in both events (a) and (b), the available time for remedial action would be sufficient (10 minutes) to assure termination of the transient by operator intervention. In the worst case condition, operator action is necessary in 10 minutes to terminate the transient and thus preclude coolant boiling in the blanket hot channel. However, with no operator action during the first 90 minutes, no structural failure in the Heat Transport System would occur, no coolant boiling in the fuel Hot Channel would occur and core coolable geometry would
49 be preserved.

DEMO Uncertainties

No specific uncertainties in the DEMO model have been identified which would be expected to significantly alter the conclusions in References
49 Q001.440-1 and 2.

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*Note that Appendix F has been withdrawn. However, the events referenced in question related to:

- F3.2.2.1 Loss of Off-Site Power with Shutdown System Failure
- F3.2.2.2 Primary Pump Trip with Shutdown System Failure
- F3.2.2.3 Intermediate Pump Trip with Shutdown System Failure
- F3.2.2.4 Inadvertent Closure of One Evaporator or Superheater Module Isolation Valve with Shutdown System Failure
- F3.2.2.5 Turbine Trip with Shutdown System Failure
- F3.2.2.6 Loss of Normal Feedwater with Shutdown System Failure 60

References:

Q001.440-1 McCall, T. B. and Markowski, F. J., "CRBRP, Response of the Plant to a Postulated Loss of One Heat Transport Loop With Failure of Both Reactor Shutdown Systems", WARD-D-0169, February 1977

Q001.440-2 McCall, T. B., Calvo, R. and Markowski, F. J., "CRBRP, Response of the Plant to Anticipated Reductions in Feedwater Flow With Postulated Failure of Both Reactor Shutdown Systems",
49 WARD-D-0170, February 1977

Question 001.441:

Assuming that a failure occurred which introduced oil into the primary sodium, but that the hydrogen was not vented to the pump cover gas, what reactivity transients are possible?

Response:

As discussed in Sections 5.3.2.3.1 and 15.4.1.3.1(E) and also in the response to Question 001.251 oil leakage into the coolant in the pump tank is high unlikely.

However, for the assumption, as stated in the question, that the bearing oil flow leaks into the sodium and that none of the hydrogen is vented to the pump cover gas, the associated reactivity consequences were estimated. Physically, such a situation cannot occur since, for the hydrogen to go into solution, some of it must enter the cover gas to provide the partial pressure required to maintain the hydrogen in solution.

In static capsule systems with diffusion controlled solution rates at about 600°F, equilibrium was attained in about ninety minutes (Reference Q001.441-1). In the pump tank, the large surface area of the bubbles, liquid turbulence, high temperature (995°F) and low hydrogen partial pressure in the cover gas, all enhance the rate of solution which will be much more rapid than in the capsules. Based on these results, it is concluded that gaseous hydrogen could only exist in sodium if the exposure time is considerably less than 90 minutes. At longer times, any hydrogen would be in solution.

For the purpose of this analysis, the cold trap was assumed not to remove any of the hydrogen from the sodium. If a pump bearing oil flow of 10c.c./hour were assumed, which is representative of the expected flow for this type of pump, then about 1.4 gms hydrogen would enter the sodium. Since the three loop flows mix in the vessel inlet plenum the resulting increase in hydrogen level is 2.9 ppb/hour. The saturation level of hydrogen in sodium at 730°F is 51 ppm (References Q001.441-2 and 3) corresponding to about 24.32 kg hydrogen in the primary sodium. It would take more than two years for the above leakage rate to reach such a hydrogen level assuming that the oil supply reservoir of the leaking pump is erroneously replenished eight times during this period, and no hydrogen is removed by the cold trap.

The resulting reactivity consequence is an up-ramp of 3.7×10^{-4} cents/hour considering the core only (0.8×10^{-4} cents/hour considering the core and blankets) to a maximum of 7.2 cents (2.0 cents considering the core and blankets) at the attainment of cold leg hydrogen saturation conditions. It is inconceivable that such a leak could continue for this length of time and that the plant would continue to operate to a hydrogen impurity of level of 51 ppm (the cold trap specification is only 0.1 ppm), however the reactivity consequences are negligible even compared to the reactivity rate due to burnup.

The consequence of delayed hydrogen solution was assessed based on the reactor products of oil/sodium tests (see Response Q001.338). The quantities of gaseous hydrogen, which dissolves in the sodium quickly, and gaseous organic reaction products, which dissolve slowly and only to a minor extent, were used to estimate a gaseous product buildup in the sodium. The quantity of hydrogen bubbles was conservatively estimated to represent the oil leakage for ninety minutes. The organic gases tend to form carbonaceous particulates which include some of the hydrogen. Conservatively these gases are assumed to continuously build-up in the coolant

The total volume of gas in the core used to assess the reactivity consequences corresponds to hot leg temperature and pressure which is conservative by a factor of about 5. For end of equilibrium cycle conditions it requires about 2900 hours of oil leakage to raise the gaseous content of the sodium to a level sufficient to cause 1 cent increase in reactivity. This is clearly inconsequential.

<u>References</u>

Q001.441-1. Meacham, S. A., Hill, E. F. and Gordus, A. A., The Solubility of Hydrogen in Sodium, APDA-241, June 1970.

Q001.441-2. Vissers, D. R. et al, A. Hydrogen Monitor for Detection of Leaks in LMFBR Steam Generators, Nuclear Technology, Vol 12, p 218, October 1971.

Q001.441-3. Methods for the Analysis of Sodium and Cover Gas, RDT F3-40, Appendix N, June 1975.

Question 001.442 (Appendix F3)

Provide the results of an analysis of the consequences of an assumed loss of off-site power and failure of both diesel-generators to operate.

Response:

The assumed loss of off-site power and failure of both diesel generators is not presently part of the Design Basis for CRBRP. Since the CRBRP design includes two off-site sources and the redundant Class 1E onsite power systems the assumed event should not be part of the design basis. This approach is consistent with past precedents for LWR's with similar design features.

Question 001.443 (F6.2.1)

Provide justification for excluding analysis of the beginning of life (BOL) core for the two design basis accidents. There are a number of reasons why one might expect an accident with greater consequences from a BOL core. For example, for a TOP accident one may expect:

a) Higher melt-fraction before pin failure;

b) Possible boiling before failure with reactivity and plugging implications;

c) No fission-gas induced fuel motion;

d) Considerably different post pin failure behavior in channels;

e) More coherence because all pins are essentially at the same burnup.

Consider items such as these when justifying the exclusion of the BOL core.

Response:* 60

The results of preliminary calculations indicate that although the details of the accident scenarios in the BOL core may differ from those calculated in Ref. 10a, PSAR Section 1.6 for the BOEC and EOEC core, the maximum accident energetics are not expected to be significantly different. 60

The preliminary analysis of the TOP event in a beginning of life core (Reference Q001.443-1) with a burnup of 73 MWd/T indicated that either a ramp rate approaching 5 $/sec or forcing the pin to fail at the core midplane would be necessary to satisfy initial condition for a hydrodynamic disassembly. It must be noted that no physical basis for initiating ramp rates approaching 5 $/sec have been identified in CRBRP. An analysis of the LOF event in a beginning of life core 23 (Reference Q001.443-2 and 3) indicated that mild initial disassemblies similar in character to those calculated in Ref. 10a, PSAR Section 1.6 were predicted with 60 moderate ramp rates at disassembly in the absence of coherent and extensive failure of pins in the low power subassemblies. Even in the latter case, the energetics were well within the structural design basis.

The effect of items a) through e) on the BOL TOP accident scenario is discussed below.

a) Higher melt fractions are expected in BOL pins prior to failure than have been calculated for pins in the BOEC and EOEC cores. The large molten fuel inventory at pin failure should introduce strong negative fuel motion reactivity effects which would be sufficient for neutronic shutdown.

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*Note that Appendix F has been withdrawn. The test upon which this question was based, can now be found in Section 4 of Reference 10a, PSAR Section 1.6. 60

b) The experimental evidence regarding failure of fresh fuel under TOP conditions indicates that failure at the low ramp rates of interest is of a thermal origin and failure occurs near the tip of the fuel column after boiling is initiated. TREAT experiments H2 and E4 (Reference Q001.443-3) provide the most direct evidence that failure of fresh fuel is associated with coolant boiling.

Therefore, the detailed mechanistic analysis of CRBRP type fuel rods confirm that the essential features of TREAT experiment behavior will be reproduced in CRBRP, cladding failure will occur after coolant boiling and near the top of the fuel column. Approximately 50-75% of the fuel will be molten at failure. Under these conditions, existing analyses (References Q001.433-4, 5) of FTR and CRBRP accident sequences agree that fuel motion toward the failure site will produce neutronic shutdown in the TOP accident.

c) Since fission gases are not present in fresh fuel, the driving force for post-failure fuel-motion in the BOL TOP case is fuel vapor pressure Because of the larger melt fraction at the time of failure in the fresh fuel, the fuel vapor pressure would act as a dispersion mechanism.

d) Once the general timing and axial location of failure are established, the key phenomena controlling the post-failure progression of the TOP accident are fuel motion toward the failure site and plug formation by fuel ejected from the fuel rod.

The FTR analysis indicates that the negative reactivity change associated with fuel motion to the failure site is sufficient to produce neutronic shutdown, even assuming no increment of fuel from the failure site. Furthermore, this reactivity effect is obtained with only 9 fuel assemblies included in the lead group and 30% of the rods in each assembly (i.e., the outer two rows of rods in each assembly) not failing because of overcooling at the assembly edges.

The geometry of the blockage is also of interest because the accident scenario depends, to some degree, upon the extent of flow blockage in the lead fuel assemblies.

If all the fuel rods in an assembly fail, a complete flow blockage may form. The FTR analysis is believed to be particularly applicable in this area because the FTR and CRBR fuel assemblies have identical pitch-to-diameter ratios and edge-rod-to-channel spacings. The FTR analysis indicates that the outer two rows of rods in each lead assembly will not fail, and partial flow will be maintained. Therefore, the events associated with complete flow blockage appear to be improbable.

e) Fuel failures in the BOL core during the TOP sequence are probably more coherent than in the BOEC or EOEC cores. However, the lack of coherence in the BOL core appears to be substantial. The FTR analysis shows that a lack of coherence within fuel assemblies, caused by the flow distribution between edge and center coolant subchannels, is sufficient to prevent the colder edge fuel rods from failing. Since lack of coherence between fuel assemblies is larger than that within fuel assemblies, lack of coherence in the BOL core is adequate to preclude greater energetic consequences than were calculated for the BOEC and EOEC cores analyzed in Ref. 10a, PSAR Section 1.6.

References

Q001.443-1 W. E. Kastenberg and M. V. Frank, "Preliminary Analysis of the Transient Overpower Accident for CRBRP with BOC 1 Fuel," Trans. Amer. Nuc. Soc., Vol 22, Nov. 1975, pp. 378-379, San Francisco, Calif.

Q001.443-2 H. H. Hummel, et. al., "Loss-of-Flow Calculations for the CRBRP Demonstration Plant," Trans. Amer. Nuc. Soc., Vol 22, Nov. 1975, P. 401, San Francisco, Calif.

Q001.443-3 Physics of Reactor Safety, Quarterly Report, January-March 1975, ANL-75-31, 1975, pp. 3-7.

Q001.443-4 "Safety Engineering Thirteenth Quarterly Report," GE-FBRD, GEAP-13923-13, November 1975.

Q001.443-5 A. E. Walter, et. al., "An Analysis of the Unprotected Transient Overpower Accident in the FTR," HEDL-TME-75-50, June 1975. 60

Question: 001.444 (F6.2.1)

Clarify the sentence starting "Differences in operating characteristics..." as it relates to comparison of LWR discharge grade Pu vs FFTF grade Pu found in Chapter 4.3 of PSAR.

Response:

This question requests clarification of information which is no longer a part of the current documentation. The Project has since consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. 60

Question 001.445a, b & c (F6.2.3.2.4)

The following requests and questions deal with the analysis and assessment of blockages found in Appendix F, in particular the SASBLOK analysis.

(a) The process of intermixing SAS and external calculations are not described in sufficient detail for an independent assessment. Provide a detailed description of the SAS-3A input, output and external calculations.

(b) The location of the blockage is assumed to be well up in the plenum. Thus full credit is taken for the (negative) fuel motion reactivity. Also, the heat source in the blockage is just the decay heat source. Justify the exclusion in your analysis of solid in-core blockages, in particular when pin failures occur in voided channels. Assume that such blockages occur and assess the progression of the accident.

(c) On page F6.2-77, it is claimed that the extensive in-core, non-porous blockages in the TREAT tests were due to the non-prototypic nature of these tests. Reconcile this conclusion with the conclusions found in ANL/RAS 74-8, that "...it is not possible to say, at this time, that the (TREAT) voiding dynamics are such as to produce more or less sweepout than would occur in a reactor".

Response:*

(a) The CRBRP Project has consolidated all considerations given Hypothetical Core Disruptive Accidents into report CRBRP-3 (References 10a and 10b, PSAR Section 1.6) and its associated references; consequently, PSAR Appendices D and F have been withdrawn in Amendments 24 and 60 respectively. The response to this question is now found in Appendix A of Reference 10a, PSAR Section 1.6. 60

(b) SASBLOK was not used to analyze solid in-core blockages resulting from failures which occurred in voided channels because none of the overpower transients (TOPs) analyzed in Reference 15, PSAR Section 1.6 were predicted to fail in such a manner that solid in-core blockages would be produced. SASBLOK was used to analyze the effects of fuel blockages where pin failures occurred in channels with full sodium flow, resulting in fuel ejection into liquid sodium, a fuel-coolant interaction, and fuel blockages due to fuel plateout. Fuel ejected into the channel under these conditions is not expected to form a complete blockage in the channel. 60 26

*Note that Appendix F has been withdrawn. The text, upon which the question was based, can now be found in Section 6 of Reference 15, PSAR Section 1.6. 60

The separate calculations of Appendix A, Reference 15, PSAR Section 1.6, can be used to assess to what degree in-core blockages can exist as a 60 function of power, sodium and geometric conditions. Fuel blockages are progressively more difficult to maintain in a stable coolable configuration as the blockage location approaches the core midplane. As the reactor power continues to increase due to the continued control rod withdrawal, blockages closer to the core are expected to be partially or totally dispersed, depending on the portion of the blockage material which cannot be cooled below the melting point.

The analysis of Section 10.1.1, Reference 15, PSAR Section 1.6 was 60 performed to assess the pessimistic assumption that fuel blockages could not be sufficiently cooled and would slump upon melting. The results of those calculations showed that slumping of the melted blockages would not result in recriticality. Therefore, the location of the blockage is not expected to have a significant effect on possible CDA energetics which might result from the slumping of melted blockages.

(c) The conclusion reached in ANL/RAS 74-8 was based upon the understanding and analysis of the TREAT tests at that time (1974). However, since then further analysis has shown that the TREAT tests would produce more blockage and less sweepout than would occur in a prototypic FFTF and CRBR reactor environment. This conclusion is based upon the fact that the TREAT pins are driven to higher melt fractions during the transient because fuel motion has no influence on the TREAT power. In addition, comparisons of the hydraulic systems have shown that a pressure surge in the core region will cause a total flow reversal in the MARK II loop of TREAT while a similar burst in the FFTF or CRBR will produce much smaller flow reversals and consequently more sweepout. These factors, evaluated after ANL/RAS 74-8 was published, indicate that the TREAT tests will produce less sweepout than would occur in a subassembly during a similar transient in the FFTF or CRBR reactor. As addressed in Section 3.2.7 of Reference 15, PSAR Section 1.6, experimental verification of this conclusion is being pursued. 26 60

Question 001.446 (F6.2.3.2.7)

Describe and discuss in sufficient detail the "overpower type experiments" referred to in line 6 of page F6.2-21.

Response:

The paragraph in which the sentence addressed in this question occurs was taken from the paper, "Current Status and Experimental Basis of the SAS LMFBR Accident Analysis Code," Proc. Am. Nuc. Soc. Fast Reactor Safety Meeting, Beverly Hills, Calif., CONF-740401-P3, pp 1303-1322 (1974), by M. G. Stevenson, et al. The "overpower type experiments" referred to in this paragraph are described in the report, "Molten Fuel Movement in Transient Overpower Tests of Irradiated Oxide Fuel," GEAP-13543 (1969), by T. Hikido and J. H. Field. Based on this report, a summary description of these experiments, which comprised the GE Series V tests, is presented in the paragraphs that follow.

This test series involved two 24-inch-active-fuel-length, 0.250-inch o.d. oxide fuel specimens (designated C5A and C5B) which were irradiated under steady-state conditions in the General Electric Test Reactor (GETR) facility to burnup goal exposures of ∿20,000 MWd/Te. Steady-state peak power for both specimens was ∿12 kW/ft for four GETR cycles. The two specimens were identical; except, the 15-inch-length upper blanket of C5A was composed of solid pellets while the C5B blanket had annular (0.070-inch i.d.) pellets to provide a potential flow path for molten fuel.

The capsules were non-destructively examined to ascertain capsule integrity and then remotely reencapsulated for transient irradiation in the TREAT facility. Peak specimen power during the transient was equivalent to 160 kW/ft for C5A and 155 kW/ft for C5B. Total specimen transient energy was 338 cal/gm for C5A and 339 cal/gm for C5B, resulting in a molten fuel volume of ∿35% for both specimens.

Specimen C5A experienced failure during the transient with extentive fuel movement into the coolant annulus, primarily in the upward direction with some fuel ≈3 inches above the fuel and blanket interface. The cladding melted in several areas with pin separation occurring near the fuel midplane.

Specimen C5B, however, survived the comparable transient exposure with no evidence of cladding failure. There was extensive upward movement (≈10 inches, measured from the fuel and blanket interface) of molten fuel into the annular blanket. A central void was formed (no central void evident after steady-state irradiation), continuous with several fuel "plugs" in the upper portion of the fuel column and intermittent in the lower portion. Molten fuel forced a separation between the uppermost fuel pellet and the blanket and filled the resulting void, coming in contact with the cladding with no adverse effects other than localized deformation.

Hikido and Field conclude that fission gases released as a result of fuel melting during transient overpower conditions contributed substantially to the driving force for relocation and dispersion of fuel.