

BAW-10174A

Topical Report  
Revision 1  
September 1992

Mark-BW Reload LOCA Analysis  
for the  
Catawba and McGuire Units

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UNITED STATES  
NUCLEAR REGULATORY COMMISSION  
WASHINGTON, D.C. 20555

May 28, 1991

Docket Nos. 50-369, 50-370  
50-413, 50-414

Mr. M. S. Tuckman, Vice President  
Nuclear Operations  
Duke Power Company  
P.O. Box 1007  
Charlotte, North Carolina 28201-1007

Dear Mr. Tuckman:

SUBJECT: SAFETY EVALUATION OF "MARK-BW RELOAD LOCA ANALYSIS FOR THE  
CATAWBA AND MCGUIRE UNITS" BAW-10174

The Duke Power Company (Duke) submitted the report "Mark-BW Reload LOCA Analysis for the Catawba and McGuire Units," BAW-10174 in September, 1989 to support operation of the Catawba and McGuire plants with fuel provided by Babcock & Wilcox Fuel Company (BWFC). The report described the adaptation of the approved B&W ECCS model for LOCA analyses and provides analytical results to support operation with the BWFC Mark-BW fuel. Duke provided additional information to clarify and supplement this report in letters dated March 27, 1990, June 7, 1990, July 25, 1990, August 8, 1990, November 7, 1990, and December 4, 1990 (BAW-10174, Revision 1, November 1990).

The NRC staff performed its evaluation of the LOCA analyses described in BAW-10174, Revision 1, with the technical assistance of the Idaho National Engineering Laboratory (INEL). The evaluation and findings of the INEL are described in detail in the INEL Technical Evaluation Report (TER) which is attached to the enclosed NRC staff safety evaluation. The TER findings, which are explicitly adopted by the NRC staff, are discussed in the enclosed NRC staff safety evaluation.

The NRC staff has concluded that BAW-10174, Revision 1, and the analyses it contains, are acceptable subject to modification of the appropriate procedures prior to operation with B&W Mark-BW fuel as discussed in Section 2.4.1 of the enclosed NRC staff safety evaluation.



Mr. M. S. Tuckman

- 2 -

This concludes our review activities in response to the submittals identified above under TAC numbers 75138 and 75139 for McGuire and 75140 and 75141 for Catawba.

Sincerely,

*Robert E. Martin*

Robert E. Martin, Senior Project Manager  
Project Directorate II-3  
Division of Reactor Projects - 1/11  
Office of Nuclear Reactor Regulation

Enclosure:  
As stated

cc/w enclosure:  
See next page



UNITED STATES  
NUCLEAR REGULATORY COMMISSION  
WASHINGTON, D.C. 20546

SAFETY EVALUATION BY THE OFFICE OF NUCLEAR REACTOR REGULATION  
RELATING TO TOPICAL REPORT BAW-10174, REVISION 1  
MARK-BW RELOAD LOCA ANALYSIS FOR CATAWBA AND MCGUIRE UNITS  
DUKE POWER COMPANY  
CATAWBA NUCLEAR STATION AND MCGUIRE NUCLEAR STATION  
DOCKET NOS. 50-413, 50-414, 50-369, 50-370

1.0 INTRODUCTION

Babcock & Wilcox Fuel Company (BWFC) will supply reload fuel to the Duke Power Company Catawba and McGuire units beginning in 1991. To support operation of the Catawba and McGuire Units with BWFC fuel, loss-of-coolant accident (LOCA) analyses for Catawba and McGuire were performed with the BWFC LOCA Evaluation Model (EM) described in the BWFC topical report, "B&W Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants," BAW-10168P, Revision 1, September 1989, and approved in the NRC staff safety evaluation report (SER) dated January 22, 1991. The adaptation of this generic model for the analyses of the Catawba and McGuire plants is described in the topical report, "Mark-BW Reload LOCA Analysis for the Catawba and McGuire Units," BAW-10174, September 1989. By letters dated March 27, 1990, June 7, 1990, July 25, 1990, August 8, 1990, November 7, 1990, and December 4, 1990 (BAW-10174, Revision 1, November 1990), the licensee provided information to clarify and supplement BAW-10174. This safety evaluation report addresses BAW-10174, Revision 1, which includes, supplements, and supersedes the original BAW-10174 document.

2.0 STAFF EVALUATION

The staff performed its evaluation of the LOCA analyses described in BAW-10174, Revision 1 with the technical assistance of the Idaho National Engineering Laboratory (INEL). The evaluation and findings are described in detail in the

INEL technical evaluation report (TER) which is enclosed as a part of this report.

## 2.1 Analysis Methodology Applicability to Catawba and McGuire

The methodology used to perform the Catawba and McGuire large-break LOCA analyses is described in BAW-10168, Revision 1 as approved with conditions as identified in the staff SERs of January 22, 1991, May 19, 1989, April 18, 1990, and August 13, 1990. Applicability of the BAW-10168, Revision 1 methodology as adapted for Catawba and McGuire was justified by the licensee as discussed in the enclosed TER, Sections 2.1 through 2.5. The TER concluded that the Catawba and McGuire LOCA analyses were performed using NRC-approved methods and computer programs, and that this use conformed to NRC conditions of approval. The staff concurs with these findings stated in TER, Section 2.6.

## 2.2 Sensitivity Studies and Spectrum Analyses (B&W Analyses)

Duke Power Company provided a number of sensitivity studies and a break spectrum analysis to justify the initial and boundary conditions and break size used in the LOCA limits analysis. The studies covered the accumulator configuration, break spectrum analysis, break type (double-ended or split break), and the maximum versus minimum ECCS study. These are discussed in the enclosed TER, Section 3.

As a result of the studies performed, the following boundary conditions were chosen for the LOCA limits analysis: Catawba accumulator configuration, guillotine break with discharge coefficient of 1.0, and the maximum ECCS flow. The TER concluded that the conditions chosen are appropriate for use in the Duke Power Company LOCA limits analysis based on the fact that the conditions that resulted in the highest PCT were chosen. The staff concurs with this TER finding.

## 2.3 Results of LOCA Analyses

### 2.3.1 Large-Break LOCA (B&W)

The licensee provided analyses and other information to address the performance requirements of 10 CFR 50.46(b). Analyses which were provided assumed a burn-up and elevation-dependent total peaking factor ( $F_q$ ) with a peak value of 2.32. The calculated peak cladding temperature assuming the above peaking factor and limiting conditions discussed in Section 2.2 is 1945°F for B&W fuel, with a corresponding maximum local oxidation of 4.9 percent, and total core-wide oxidation of 0.55 percent. These values are within the performance requirements of 10 CFR 50.46(b)(1), (2), and (3) of 2200°F, 17 percent, and 1 percent, respectively. The licensee also addressed 10 CFR 50.46(b)(4) and (5) regarding maintenance of coolable geometry and long term cooling. The enclosed TER discusses these considerations in TER Section 4.1 and concludes that the large-break LOCA analyses for B&W fuel are in conformance with the requirements of 10 CFR 50.46(b). The staff concurs with this finding.

### 2.3.2 Large-Break LOCA (Westinghouse)

The LOCA analyses of record for Catawba and McGuire indicate that the calculated peak cladding temperatures for Catawba and McGuire are 1704°F and 1841°F, respectively. These values are lower than the PCT for B&W fuel.

### 2.3.3 Small-Break LOC

The licensee provided information and qualitative analyses to show that the B&W fuel peak cladding temperature (PCT) response to a small-break LOCA would be similar to the response of Westinghouse fuel. The licensee's analyses indicate that the small-break LOCA PCT spectrum is bounded by the large-break LOCA spectrum for both Westinghouse and B&W fuel. Small-break LOCAs are discussed in Section 4.2 of the attached TER. The TER concludes that small-break LOCAs are not limiting for Catawba and McGuire. We concur with the TER findings.

#### 2.3.4 Mixed Core LOCA Effects

The licensee provided information and qualitative analyses which indicate that the large-break LOCA analysis for a core composed entirely of B&W fuel bounds analyses for any mixed core combination of B&W and Westinghouse fuels. This is discussed in Section 4.3 of the enclosed TER. The TER concludes that the licensee's results are acceptable. The staff concurs with this TER finding. However, the staff limits this concurrence as applicable only to the specific types of B&W (Mark-BW) and Westinghouse fuel (DFA) addressed in the analyses because other vendor fuel series may have different thermal-hydraulic characteristics.

#### 2.4 Conditions of Acceptance Identified in TER

The enclosed TER identifies two conditions of acceptance among its conclusions in TER Section 5. These will be satisfied by Catawba and McGuire as discussed below.

2.4.1 TER, Section 5, conclusion 6 identifies that the boron precipitation time and the time to post-LOCA hot leg recirculation was recalculated and set at 9 hours because of the reduction in core coolant volume due to the use of Mark-BW fuel. By letter dated May 8, 1991, the licensee committed that the next FSAR updates for the Catawba and McGuire and their respective emergency procedures for operation with B&W Mark-BW fuel will reflect the amended calculation. The staff finds this commitment acceptable to satisfy this condition of acceptance.

2.4.2 TER, Section 5, conclusion 7 identifies that the plant should be operated within the bounds set by analysis limits for the Doppler and Moderator density coefficients. Operation within the Technical Specifications limits for the plants, as reviewed and reported in separate staff evaluations, assure conformance with this condition of acceptance. The staff finds this condition satisfied.

### 3.0 CONCLUSIONS

Based on our review, summarized above, we have concluded that the licensee has appropriately identified the bounding LOCA event, a double-ended cold leg guillotine (DECLG) break with a break discharge coefficient ( $C_d$ ) of 1.0, and assuming maximum ECCS flow. This event also assumes a core fully loaded with B&W Mark-BW fuel operated at a burnup and elevation dependent total peaking factor ( $F_q$ ) with a peak value of 2.32. The licensee has properly analyzed this case using an approved model conforming with the requirements of 10 CFR 50 Appendix K consistent with conditions of acceptance of the model. Results of these calculations, PCT=1945°F, peak local cladding oxidation of 4.9 percent, and total core-wide oxidation of 0.55 percent are within the limits specified in 10 CFR 50.46(b)(1), (2), and (3) of 2200°F, 17 percent, and 1 percent, respectively. The core geometry will remain amenable to cooling, and long-term cooling is assured, as required by 10 CFR 50.46(b)(4) and (5), respectively, with the implementation of the revised boron precipitation calculated time (see Section 2.4.1). The staff therefore finds the LOCA analyses for Catawba and McGuire described in the topical report BAW-10174, Revision 1, acceptable.

Principal Contributor: F. Orr

Dated:

EGG-EAST-9410

TECHNICAL EVALUATION REPORT  
FINAL REVIEW OF LOCA ANALYSES FOR THE  
DUKE POWER COMPANY  
CATAWBA AND MCGUIRE UNITS  
WITH  
BABCOCK & WILCOX FUEL COMPANY RELOAD FUEL  
BAW-10174, REVISION 1

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May 1991

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## ABSTRACT

Babcock & Wilcox Fuel Company (BWFC) will supply reload fuel to the Duke Power Company Catawba and McGuire units beginning in 1991. To support operation of the Catawba and McGuire units with BWFC fuel, loss-of-coolant accident (LOCA) analyses for Catawba and McGuire were performed with the BWFC recirculating steam generator (RSG) LOCA Evaluation Model (EM) and reported in BAW-10174, Rev. 1, Mark-BW Reload LOCA Analysis for the Catawba and McGuire Units. Duke Power Company also supplied information in the above report to justify operation of the Catawba and McGuire units during the transition period while both Westinghouse Optimized Fuel Assemblies and BWFC Mark-BW fuel assemblies reside in the core. The information was reviewed to determine whether the acceptance criteria of 10 CFR 50.46 are met with the Mark-BW reload fuel and during the transition period. The information was also reviewed to determine that Nuclear Regulatory Commission approved methods were used in the LOCA analyses. The review found accepted methods were used, and the LOCA analyses provided by Duke Power Company demonstrated the acceptance criteria of 10 CFR 50.46 were met. Therefore, the INEL recommends the report be accepted to support operation of the Catawba and McGuire units with BWFC reload fuel provided certain conditions are met.



## SUMMARY

Babcock & Wilcox Fuel Company (BWFC) will supply reload fuel to the Duke Power Company Catawba and McGuire units beginning in 1991. To support operation of the Catawba and McGuire units with BWFC fuel, loss-of-coolant accident (LOCA) analyses for Catawba and McGuire were performed with the BWFC recirculating steam generator (RSG) LOCA Evaluation Model and reported in BAW-10174, Rev. 1, Mark-BW Reload LOCA Analysis for the Catawba and McGuire Units. Duke Power Company also supplied information in the above report to justify operation of the Catawba and McGuire units during the transition period while both Westinghouse Optimized Fuel Assemblies and BWFC Mark-BW fuel assemblies reside in the core. Information on small-break LOCAs (SBLOCAs) was also provided by Duke Power Company. For SBLOCAs, Duke Power Company justified that previous SBLOCA analyses performed for Westinghouse fuel remained bounding for the BWFC reload fuel.

The information provided by Duke Power Company was reviewed to determine whether the Catawba and McGuire units meet the acceptance criteria of 10 CFR 50.46 with the Mark-BW reload fuel and during the transition period. This was done for both large- and small-break LOCAs. The review also determined whether Duke Power Company used Nuclear Regulatory Commission approved analysis methods to determine compliance.

The review found that approved methods were used by Duke Power Company. Review of the information provided by Duke Power Company found mixed core operation was adequately justified and the acceptance criteria of 10 CFR 50.46 were met for large- and small-break LOCAs:

1. Peak cladding temperatures were less than 2200°.
2. Maximum local cladding oxidation was less than 17%.
3. Core wide oxidation was less than 1%.
4. The core geometry remains amenable to cooling.
5. Long-term cooling was assured.

Therefore, the INEL recommends the report be accepted to support operation of the Catawba and McGuire units with BWFC reload fuel provided certain conditions are met.

## PREFACE

This report was prepared for the U.S. Nuclear Regulatory Commission, Office of Nuclear Reactor Regulation, by EG&G Idaho, Inc., Energy and Systems Technology Group.

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TECHNICAL EVALUATION REPORT  
FINAL REVIEW OF LOCA ANALYSES FOR THE  
DUKE POWER COMPANY  
CATAWBA AND MCGUIRE UNITS  
WITH  
BABCOCK & WILCOX FUEL COMPANY RELOAD FUEL  
BAW-10174, REVISION 1

1. INTRODUCTION

Babcock & Wilcox Fuel Company (BWFC) will supply reload fuel to the Duke Power Company Catawba and McGuire units beginning in 1991. To support operation of the Catawba and McGuire units with BWFC fuel, large-break loss-of-coolant accident (LBLOCA) analyses for Catawba and McGuire were performed with the BWFC recirculating steam generator (RSG) LOCA Evaluation Model (EM)<sup>1</sup> and reported in Reference 2. Plant performance during small-break LOCAs (SBLOCAs) was addressed by demonstrating the previous SBLOCA analyses for Westinghouse fuel still apply to BWFC Mark-BW reload fuel. In Reference 2, Duke Power Company also supplied information to justify operation of the Catawba and McGuire units during the transition period when both Westinghouse Optimized Fuel Assemblies (OFA) and BWFC Mark-BW fuel assemblies reside in the core. Duke Power Company submitted Reference 2 to the Nuclear Regulatory Commission (NRC), Office of Nuclear Reactor Regulation (NRR), for review and approval by NRR to justify continued operation of the Catawba and McGuire units with BWFC reload fuel.

The Office of Nuclear Reactor Regulation requested the assistance of the Idaho National Engineering Laboratory (INEL) in reviewing Duke Power Company's submittal for Catawba and McGuire. Specifically, the INEL was requested to review:

1. Whether Duke Power Company used NRC approved analysis methods.
2. Whether the Catawba and McGuire units meet the acceptance criteria of 10 CFR 50.46 with the Mark-BW reload fuel and during the transition period.

Related to the above reviews, NRR also requested INEL review and evaluate Duke Power Company's responses to NRC questions regarding the Catawba and McGuire LOCA analyses. The NRC questions were transmitted to Duke Power Company in Reference 3. The responses from Duke Power Company are contained in References 4 to 8.

This technical evaluation report documents the review of the Catawba and McGuire LOCA analyses with BWFC reload fuel. Section 2 of this report documents the review of the methods used by Duke Power Company to complete the LOCA analyses for Catawba and McGuire. Sensitivity studies and the spectrum analysis are discussed in Section 3. Conformance of the analysis results to the acceptance criteria of 10 CFR 50.46 for large- and small-break LOCAs is discussed in Section 4. Justification of the operation of Catawba and McGuire with mixed cores is also discussed in that section. The conclusions of the review and references are listed in Sections 5 and 6, respectively.

## 2. ANALYSIS METHODS

This section discusses the analysis methods used by Duke Power Company to perform the Catawba and McGuire LOCA analyses. The methods were reviewed for NRC approval and conformance to conditions of approval imposed as a result of the NRC review. The methods reviewed include the overall BWFC RSG LOCA EM and the following computer programs: RELAP5/MOD2 - B&W,<sup>9</sup> FRAP-T6-B&W,<sup>10</sup> REFLOD3B,<sup>11</sup> and BEACH.<sup>12</sup> The above methods will be discussed in the listed order.

### 2.1 Overall BWFC RSG LOCA EM

The overall BWFC RSG LOCA EM is described in Reference 1. As described in that report, the BWFC RSG LOCA EM uses a series of codes to calculate the system response to a large-break LOCA. The methodology was approved by the NRC.<sup>13</sup> A number of the NRC conditions of approval in Reference 13 are related specifically to SBLOCA analyses. See Items 2,j,k,n; 4, 5, and 9 to 12, in Section 4 of the technical evaluation report (TER) enclosed with Reference 13. Because Duke Power Company justified the Westinghouse SBLOCA analyses remain bounding for Catawba and McGuire with Mark-BW reload fuel, those requirements are not discussed in this report. The following conditions were imposed on the LBLOCA methodology as a result of the NRC review.

1. The following items need to be addressed by Duke Power Company in the plant-specific submittal:
  - a. Justification for taking the initial rod pressure from the steady state fuel code or a use of a bounding pressure. Duke Power Company used the initial rod pressure from the NRC approved steady state fuel code TAC03<sup>14,15</sup> in their evaluations.
  - b. Data to be used for the fuel temperature and moderator void reactivity void coefficients will be provided and justified. This information was provided by Duke Power Company in their response to question 2, Reference 4. The Doppler temperature coefficient used was -2.2 pcm/°F. This value is the most



negative bounding value for beginning-of-life (BOL) core conditions. According to Reference 4, the most negative Doppler coefficient at BOL is greater than  $-1.9 \text{ pcm}/^{\circ}\text{F}$ . At end-of-life (EOL), the Doppler coefficient may approach  $-2.2 \text{ pcm}/^{\circ}\text{F}$ , but according to Duke Power Company the moderator density coefficient at EOL compensates for this variation. Duke Power Company stated the moderator reactivity effect is dominated by the density variation and the moderator temperature effect on reactivity is not used (the most positive temperature coefficient allowed by the Technical Specifications is  $0.0 \text{ pcm}/^{\circ}\text{F}$ ). The moderator density reactivity is also based on the most positive coefficient allowed by the Technical Specification limits. Using the NULIF code,<sup>16</sup> the boron concentration at BOL was varied until the temperature coefficient was  $0.0 \text{ pcm}/^{\circ}\text{F}$ ; then the density was varied to obtain the density effect on reactivity. This resulted in a curve, provided in Duke Power Company's response to question 2, that varied the moderator density coefficient from  $0.0 \text{ } \Delta k/k$  at a relative density of 1.0 to  $-20.854 \text{ } \Delta k/k$  at a relative density of 0.2. Provided the plants are operated within the limits set by the above analysis parameters, the Doppler and Moderator density coefficients used by Duke Power Company are adequate.

- c. Effect of pump trip on LBLOCAs will be evaluated. Duke Power Comp. responded to this issue in question 16, Reference 5. They provided the results of a study that evaluated the effects of leaving the pumps on or tripping them off during the blowdown phase. The results of the study showed that, although there were differences between the two cases early in the blowdown, the results for the two cases had converged by the end-of-blowdown. The pumps tripped case gave slightly higher fuel and cladding temperatures relative to the case with the pumps left on. With respect to the reflood phase, Duke Power Company noted that a study in the BWFC RSG LOCA EM topical report (see Section A.2.4, Reference 1) had already shown that a free spinning pump rotor



was a benefit to the analysis. Therefore, Duke Power Company concluded, and the INEL agrees, that the pumps tripped case is the most limiting for LBLOCA's.

To clarify the response to question 16, a telecon was held on December 14, 1990 with BWFC, who performed the LOCA analyses for Duke Power Company, to discuss the effect of pump two-phase degradation on the pumps on/off study. Babcock & Wilcox Fuel company noted the study discussed above was performed using the pump degradation multipliers identified in the BWFC RSG LOCA EM report. That study assumed the pump were tripped. Based on the EM work and the study for Catawba and McGuire, BWFC concluded that pump operation has only a small effect on the overall transient relative to other parameters. Therefore, BWFC felt no further work was needed. The INEL reviewed the analyses provided by BWFC and Duke Power Company that considered pump effects and concluded the effect of improved pump performance (leaving the pumps on or decreased two-phase degradation) in the BWFC EM is such that it reduces the calculated PCT. In the pump two-phase degradation study in Reference 1, the PCT was approximately 50 to 75°F lower for the case with the least two-phase degradation (see Figure A-36). In the Catawba and McGuire pumps on/off study, the PCT was approximately 22°F lower for the pumps on case (see Figure 16-6, Reference 5). Therefore, INEL considers use of the two-phase degradation multipliers identified assuming the pumps were off and pump trip at the start of the transient adequate for use in the pumps on/off study.

- d. The containment pressure for the LBLOCA analyses will be provided and justified on a plant-specific basis. The Catawba containment pressure reported in the Final Safety Analysis Report (FSAR) associated with the maximum emergency core cooling system (ECCS) injection case was used for the LBLOCA analyses reported in Reference 2. Duke Power Company provided additional information to support this choice in their response to question 8, Reference 6. They noted the mass and energy release to the

containment for the current LBLOCA evaluations was greater than for the Catawba FSAR analysis. Thus, a containment pressure analysis based on the current mass and energy releases would have been higher than the FSAR curve. The higher pressure would result in lower peak cladding temperatures (PCTs) during the reflood phase of the analyses. Duke Power Company stated the containment heat structures and heat transfer surfaces were recalculated in 1987, the recalculated values were used in the minimum containment pressure analysis for the reload calculations, and they periodically review the results to verify the calculations remain conservative with respect to subsequent plant modifications. Duke Power Company also compared the Catawba and McGuire minimum containment pressure analyses and verified the Catawba analysis was lower. Therefore, INEL considers use of the Catawba FSAR containment pressure adequate to ensure a conservative containment pressure was used in the LOCA analyses.

- e. The ECCS single failure applied to the LBLOCA EM calculation will be justified on a plant-specific basis. The ECCS single failure chosen for the Catawba and McGuire analyses was the no failure option. The spillage to the containment from the additional injection resulted in a lower containment pressure, lower reflood rate, and higher PCT.

To clarify the information provided on the single failure in Reference 2, a telecon was held with BWFC on December 14, 1990 to clarify the effect of pump operation, pumps on or locked, on the single failure chosen for analysis. In the call, BWFC noted the single failure assumption is dominated by reflood effects, not blowdown effects. Therefore, the maximum ECCS flow case still is the dominant failure because it results in a lower containment pressure. Also, BWFC pointed out the pump locked rotor assumption already has been shown to be the most severe condition for the reflood phase. With regard to the minimum containment pressure calculation, BWFC noted that the NRC Containment Systems Branch (CSB) requires that the containment sprays and fans be on

at their maximum capacity to maximize the effect on containment pressure. The INEL reviewed the pump rotor study in Reference 1, and this confirmed the statement by BWFC: the locked rotor is the worst case for reflood. Branch technical position: CSB 6-1, Minimum Containment Pressure Model for PWR ECCS Performance Evaluation, was also reviewed. This review confirmed BWFC's statement on the NRC CSB required assumptions for the containment sprays and fans for all containment pressure analyses. Therefore, INEL finds the single failure analysis by Duke Power Company adequate for LOCA analyses.

- f. The values used in the REFLOD3B calculations for the fuel and cladding conductivities and the gap heat transfer coefficient will be justified in the plant-specific applications. Duke Power Company addressed this item in their response to question 1, Reference 4. They stated the gap conductance and the fuel and cladding conductivities were selected to maximize heat transfer in order to minimize the reflood rate. The gap conductance and the cladding conductivity increase with temperature. Because the average gap and cladding temperatures during reflood did not exceed 1600°F, that temperature was used to determine the gap conductance and cladding conductivity. In addition the gap conductance was set assuming the minimum cold gap width, 3.25 mils. Based on these parameters, the gap conductance was set to 827 BTU/ft<sup>2</sup>-hr-°F and the cladding conductivity was set to 13.698 BTU/ft-hr-°F. The fuel conductivity decreases with increasing temperature. Therefore, the fuel conductivity was determined assuming a lower bound average fuel temperature of 800°F; this resulted in a fuel conductivity of 2.628 BTU/ft-hr-°F. Because the average fuel temperature dropped below 800°F late in the reflood analysis, Duke Power Company reported the results of a study where the fuel conductivity was based on a temperature of 400°F. They noted the effect on the reflood rate was less than 1%. Therefore, Duke Power Company concluded use of a fuel conductivity based on 800° was acceptable. Because of the small sensitivity for the

fuel conductivity and the conservative gap conductance and cladding conductivity used, these parameters are adequate.

- g. The effects of the maximum pumped ECCS flow versus the minimum pumped ECCS flow will be studied on a plant-specific basis. The results of this study will be provided to the NRC for review with Duke Power Company's justification for the pumped ECCS flow rates used in the LBLOCA EM analyses. See item e above.
- h. Justification should be provided in the LBLOCA EM plant-specific submittal that an end-of-life (EOL) blowdown rupture will not occur if an EOL calculation is not supplied. Also, the core crossflow resistance study is not considered valid for blowdown ruptures. In Reference 2, it was stated the EOL volume-average fuel temperature for Catawba and McGuire was slightly less than 1800°F and the rod pressure was 2280 psia. This compares to an EOL volume-average fuel temperature of 1911°F and a rod pressure of 2280 psia for the EOL case in the BWFC RSG LOCA EM report. Because a blowdown rupture did not occur during the analysis reported in the EM topical report and because those conditions bound the conditions at Catawba and McGuire, Duke Power Company concluded that a blowdown rupture would not occur at Catawba and McGuire. The INEL agrees with Duke Power Company's conclusion and considers the information supplied in Reference 2 adequate to resolve the question of a blowdown rupture occurring at EOL.
- i. A separate REFLOD38 nodalization study will be completed prior to the first application of the LBLOCA EM to a Combustion Engineering (CE) 2 x 4 plant to confirm the nodalization scheme. Because Catawba and McGuire are Westinghouse four-loop plants, this requirement does not apply.
- j. Unless justification can be provided that the current licensing base and procedures are still applicable to BWFC reload fuel, the details of the long term cooling methodology will need to be

justified with the first application of the method to a RSG plant. If a plant-specific long term cooling analysis needs to be performed by Duke Power Company, the analysis methods and results should be justified as discussed in Sections 2.1.6 and 2.2.6 of the TER enclosed with Reference 13. Technical specifications that could affect the long term cooling analyses for Catawba and McGuire were not changed with the reload fuel; therefore, new long term cooling analyses were not performed for the plants.

- k. In their RSG LOCA EM, BWFC credited control rod insertion after the end-of-blowdown in justifying their approach to calculating fission power during the refill and reflood portions of a LBLOCA. Because the NRC Standard Review Plan<sup>17</sup> requires an analysis justifying control rod insertion at the time it is credited, Duke Power Company should justify on a plant-specific basis the control rod insertion credited in BWFC RSG LOCA EM. This should include justification that the core and upper vessel geometry will permit control rod insertion at the time credited in the analysis. Duke Power Company discussed this item in their response to question 4, Reference 6. A review of the loads on the upper vessel structures for Catawba determined that the insertion of all control rods during reflood could not be guaranteed. However, they noted there was sufficient negative reactivity inserted by sources other than the control rods to provide the negative reactivity assumed in BWFC's analysis in the BWFC RSG LOCA EM without having to credit control rod insertion.
- l. The plant models used by BWFC for SBLOCA EM analyses use dual flow path junction models for the vessel connections to the hot and cold legs. This modeling approach may also be used for LBLOCA EM analyses. Use of dual flow path junction models is not recommended by the RELAP5/MOD2 code developers because of the possibility of unphysical recirculation flows. However, BWFC stated in their RSG LOCA EM that this approach will be used in RELAP5/MOD2 - B&W plant analyses. Therefore, BWFC committed to



plotting all junctions within dual flow path groupings and examining the calculated results for unphysical flow patterns. If any unphysical results are found, then corrective measures will be taken, justification for any EM deviation provided, and NRC approval for the deviation sought. This is adequate to ensure unphysical recirculation due to the dual flow path model will be identified and corrected. However, the use of two junctions at the hot and cold leg connections also decouples the interaction of the steam and liquid. Therefore, BWFC should also review the results to ensure liquid draining is calculated correctly in cases where the steam flow is sufficient to impede or prevent liquid drainback. For Catawba and McGuire, Duke Power Company justified the current SBLOCA analyses for the plants are still applicable. Therefore, for SBLOCAs this requirement does not apply. For LBLOCAs, the dual flow path model was used for the hot leg to vessel connections. In a call with Duke Power Company and BWFC on November 19, 1990, it was stated that the junction flows were reviewed and no unphysical results found. In a call with BWFC on December 20, 1990 it was stated that the hot leg to vessel flows were reviewed and countercurrent flow was correctly treated with the dual flow path model. Babcock & Wilcox Fuel Company reached this conclusion by looking at the steam velocity in the lower path when countercurrent flow was calculated in the lower path and comparing it to the steam velocity in the upper path. In all cases, the steam flow in the upper path did not exceed the steam flow in the lower path. Therefore, BWFC concluded the dual flow path model did not result in accelerated draining of the hot leg for the large-break LOCA limits analyses. Thus, this requirement was met for LBLOCAs.

- m. The proposed method to meet Criterion 4 of 10 CFR 50.46 is adequate for LOCA EM analyses and the effect of PCT on core geometry. However, core geometry concerns for a combined seismic and LOCA loads event also need to be addressed in the plant-specific submittal. This concern was addressed by Duke Power Company in Chapter 10 of Reference 2.

2. The BWFC RSG LOCA EM does not account for prerupture cladding strain in RELAP5/MOD2 - B&W and BEACH. This position was accepted in the review of the LOCA EM. However, BWFC was required to justify the acceptability of any licensing analyses where cladding swell exceeded 20% but rupture was not calculated. In the Catawba and McGuire LBLOCA analyses, fuel rod rupture was calculated. For SBLOCAs, the current SBLOCA were justified as still being applicable.
3. During the BWFC RSG LOCA EM review, the INEL noted the FRAP-T6-B&W analysis of Semiscale LBLOCA experiment S-04-6 had temperatures approximately 250°F different at two elevations that had the same power. In a telephone call with BWFC on the EM on August 24, 1990, BWFC stated the differences were the result of the data transfer from RELAP5/MOD2 - B&W to FRAP-T6-B&W and the interpolation of the core conditions passed between the codes with different core axial noding. Because this situation has the potential to result in the calculation of lower and, thus, nonconservative PCTs, the NRC required BWFC to review LBLOCA EM analyses where the codes involved in the analysis use different core axial noding to detect discrepancies in heat transfer regimes that may result from the data transfer. In a call with Duke Power Company and BWFC on November 19, 1990, BWFC stated they reviewed each LOCA limit run and the node temperature patterns made sense; the temperature patterns were explainable by power differences between nodes, location of rupture, and grid locations. For example for the 2.9 ft LOCA limits case, prior to rupture, nodes 8 and 9 were within 50°F and nodes 11 and 12 were within 80°F. Therefore, it appears heat transfer regime discrepancies did not occur in the Catawba and McGuire LBLOCA analyses. This satisfies the NRC concern.

## 2.2 RELAP5/MOD2 - B&W

The BWFC RSG LOCA EM used RELAP5/MOD2 - B&W to calculate the system LBLOCA thermal-hydraulic blowdown response. The code was approved by the NRC.<sup>18</sup> A number of the NRC conditions of approval in Reference 18 are related specifically to SBLOCA analyses. See Items 5 and 6 in Section 4 of Reference 18. Because Duke Power Company justified the Westinghouse SBLOCA

analyses remain bounding for Catawba and McGuire with Mark-BW reload fuel, those requirements are not discussed in this report. The RELAP5/MOD2 - B&W code was approved with the following LBLOCA restrictions:

1. The CSO film boiling correlation in the core heat transfer model should not be used for licensing applications without additional review and approval by the NRC. According to the BWFC RSG LOCA EM, the Condie-Bengston film boiling correlation is used in the core heat transfer model. Therefore, this requirement was met.
2. Prerupture cladding swell is not modeled because BWFC indicated it is generally less than 20% with insignificant flow diversion effects. The NRC safety evaluation report (SER) stated the acceptability of neglecting prerupture cladding swell would be resolved as part of the LOCA EM review. That review concluded the neglect of prerupture cladding swell is acceptable. Therefore, the NRC condition was met.
3. For use in LOCA licensing calculations, the NRC SER required the use of static properties for the Extended Henry-Fauske and Moody critical flow models. The BWFC LOCA EM requires the use of static properties with the above models. Therefore, the NRC requirement was met.
4. The NRC SER required BWFC to ensure the decay heat used in LOCA licensing analyses complied with Appendix K, i.e., use 1.2 times the 1971 ANS standard for decay heat. In response to question 8, Reference 19, BWFC compared the RELAP5/MOD2 - B&W built-in decay heat curve to the 1971 standard. The comparison showed that the built-in data adequately represented the 1971 curve. Therefore, if the built-in data was used in the Catawba and McGuire submittal, it satisfies the NRC concern.

### 2.3 FRAP-T6-B&W

FRAP-T6-B&W was used in the BWFC RSG LOCA EM to calculate the hot rod response (PCT and local oxidation) from the beginning of the accident through the end of refill. The code was approved by the NRC<sup>20</sup> with the following restriction.



The NRC required the time steps used in LOCA analyses be consistent with BWFC's response to question 3, Reference 21. Babcock & Wilcox Fuel Company's response to question 3 stated the time steps used comply with INEL guidelines<sup>22</sup> and were validated by sensitivity studies discussed in the BWFC LOCA EM report. The sensitivity studies were discussed in BWFC's response to question 28, Reference 23, where BWFC provided the specific time steps required to be used in LOCA analyses. Use of these time steps satisfies the NRC concern.

#### 2.4 REFLOD3B

REFLOD3B was used to calculate the system refill and reflood response. In particular, the core reflood rate calculated by REFLOD3B is used as a boundary condition by BEACH in calculating the reflood hot rod response (PCT and local oxidation). Revision 2 of the code was approved by the NRC<sup>24</sup> with the following restrictions:

1. The REFLOD3B SER required the parameter  $C_{sup}$  be  $\geq 1.05$  in REFLOD3B analyses. However, the current version of the BWFC RSG LOCA EM requires  $C_{sup}$  be  $\geq 1.025$ . Babcock & Wilcox Fuel Company provided sufficient information regarding this difference in their response to a question (question 4, Reference 19) on the BWFC RSG LOCA EM that the NRC accepted  $C_{sup} \geq 1.025$  for use in LOCA analyses in Reference 13.
2. The NRC SER required the parameters  $C_T$  and  $C_H$  be equal to  $1.0 \text{ sec}^{-1}$  and  $1.0988 \text{ ft}^{-1}$ , respectively, for LOCA analyses. The BWFC RSG LOCA EM requires that  $C_T$  and  $C_H$  be set at the required values.
3. A value of 1.0 for the vent valve steam condensation efficiency was required by the NRC SER when the vent valve model is used to represent upper head leakage paths. This is the value required by the BWFC RSG LOCA EM.
4. The CRF4 carryover rate fraction correlation and the BWF reflood heat transfer correlation are not to be used for EM calculations. Also, option 1 of the REFLOD3B cladding surface heat transfer correlations

is not to be used for LOCA analyses; rather, option 2 which includes the CRFCKN carryover fraction correlation and the ANC/FLECHT heat transfer correlations are required to be used. Babcock & Wilcox Fuel Company's RSG LOCA EM requires the NRC specified options be used for LOCA analyses.

Also, Revision 3 of the REFLOD3B code was approved in Reference 13 without additional restrictions.

## 2.5 BEACH

BEACH was used to calculate the reflood hot rod response (PCT and local oxidation). Because of BWFC RSG LOCA EM changes, the version of the BEACH code used in the Catawba and McGuire analyses also changed. The final LOCA limits calculations used to justify conformance to the acceptance criteria of 10 CFR 50.46 (see Chapter 8 of Reference 2 and Section 4 of this report) used the code version approved by the NRC<sup>25</sup> with the following restrictions:

1. The acceptability of any licensing analyses where cladding swell exceeded 20% but rupture was not calculated should be justified. For the LBLOCA analyses, rupture was calculated for the LOCA limits calculations. Therefore, this requirement does not apply.
2. In using the revised grid and rupture models with the recommended empirical values of the droplet breakup number,  $n$ , and the volume length constant,  $c_1$ , given in Reference 12, the user should ensure the models are applied to plant conditions within the applicable ranges for which the empirical constants were assessed or must supply additional justification for their use. The applicable ranges were given in the NRC SER. Based on the information in Reference 2 and References 4 to 8 and obtained in a call with BWFC, the company that performed the LOCA limits analyses for Duke Power Company, on December 14, 1990, the INEL concluded the code was applied within the ranges specified in the SER.

3. If the multiplier gap conductance model in the previous version of BEACH is used in licensing calculations rather than the acceptable models for dynamic gap conductance and cladding swell and rupture, further justification will be required as specified in the the staff SER on Revision 1 of BAW-10166P. Based on the call with BWFC on December 14, 1990, the multiplier gap conductance model discussed in the NRC SER was not used.

In the December 14, 1990 call, BWFC noted there is the possibility of confusion over the meaning of the gap multiplier model as it applies to BEACH analyses. This is because there are two gap multiplier models in the code, one which is used in BEACH analyses and one which is not used based on the NRC SER. The first model is used to initialize the fuel rod model in BEACH to give the same initial stored energy as the TACO3 steady state fuel code. This is the same multiplier model used in RELAP5/MOD2 - B&W. The second model was implemented in an earlier version of BEACH and allowed the code user to vary the gap conductance axially to simulate rupture effects. This was the model discussed in the NRC SER. The INEL notes that use of the first model, to initialize the rod model to the TACO3 results, was accepted in the RELAP5/MOD2 - B&W review. Therefore, it is not a concern to use it with BEACH. Also, BWFC indicated the second model is no longer used in BEACH analyses because of the addition of the dynamic gap conductance model from RELAP5/MOD2 - B&W.

## 2.6 Summary

Based on the review described above, the Catawba and McGuire LOCA analyses were completed using NRC approved methods and computer programs. Also, use of the the methods and codes conformed to any NRC conditions of approval.

### 3. SENSITIVITY STUDIES AND SPECTRUM ANALYSIS

Because of the similarities between Catawba and McGuire, the LOCA analyses reported in Reference 2 were performed with a single model representing both plants. However, the INEL noted that the Westinghouse FSAR LBLOCA analyses indicated the PCT was 137°F lower for Catawba relative to McGuire. To clarify the reasons for this difference between the previous LOCA results and the current single model approach for Catawba and McGuire, Duke Power Company noted that plant-specific differences between the two plants could account for the difference in PCT (see question 9, Reference 5). The major differences between the two plants are the accumulator flow (Catawba higher than McGuire) and pumped injection flow rate late in the reflood period (McGuire higher than Catawba). These differences were related back to the Westinghouse PCT calculations by noting that the initial temperature rise during reflood for Catawba turned over at a lower temperature relative to McGuire because of the larger accumulator flow. However, because of the lower pumped emergency core coolant (ECC) flow late in the reflood period, Catawba experienced a second temperature increase where the PCT was calculated. For McGuire, however, once the cladding temperatures peaked early in the reflood phase and started to decrease, they continued to decrease for the rest of the reflood phase. Because the PCT occurred early for McGuire and late for Catawba, the INEL notes that differences in decay heat levels would also be a factor in calculating the different PCTs. Duke Power Company noted in their response to question 9 that sensitivity studies performed by BWFC were used to account for the Catawba and McGuire accumulator and pumped ECC injection differences in a worst case manner. However, because of differences in Eqs between Westinghouse and BWFC, this resulted in different results relative to the PCT calculation as discussed in Section 3.1 of this report.

A number of sensitivity studies and the break spectrum analysis were provided by Duke Power Company to justify the initial and boundary conditions and break size used in the LOCA limits analysis. The studies covered the accumulator configuration, break spectrum analysis, break type (double-ended or split break), and the maximum versus minimum ECCS flow study. They will be discussed in that order.

### 3.1 Accumulator Configuration

One area where Catawba and McGuire differ is in the accumulator configuration. The differences are due to different gas and liquid volumes and different resistance factors in the accumulator lines. Catawba has a larger liquid volume (1050 versus 950 ft<sup>3</sup>), smaller gas volume (350 versus 450 ft<sup>3</sup>), and lower line resistance factor (5.7 versus 12.4) compared to McGuire. To determine which plant's accumulator configuration is most limiting with respect to PCT, a study was performed where both configurations were analyzed. The PCT was 4°F higher with the Catawba accumulator configuration, and the Catawba configuration was chosen for further review.

This result is different from that discussed above; the Catawba accumulator configuration resulted in a lower PCT for Catawba relative to McGuire in the Westinghouse FSAR analyses. Duke Power Company noted there were two differences between the Westinghouse and BWFC EMs that could account for this (question 9, Reference 5). First, the BWFC EM only allows the ECC to fill the downcomer to the bottom of the cold leg nozzles while the Westinghouse model allowed the downcomer level to increase above the bottom of the cold leg nozzles. Thus, the additional accumulator injection in the Westinghouse analysis for Catawba was able to provide additional downcomer driving head for the core reflood that was not possible in the BWFC analysis. Second, because the BWFC analysis assumed complete condensation of steam on the accumulator injection in the cold leg, the additional accumulator flow, once the downcomer was full, represented a penalty and not a benefit as in the FSAR analysis.

### 3.2 Break Spectrum Analysis

The break spectrum analysis was performed for a double-ended guillotine break with discharge coefficients of 1.0, 0.8, and 0.6 applied to the break flow. The case with a discharge coefficient of 1.0 resulted in the highest PCT.

Because the break spectrum analysis was completed before the BWFC evaluation model was finalized, Duke Power Company discussed why they



considered the results of this spectrum analysis valid even for the final evaluation model. In reviewing the effects of the EM changes, Duke Power Company concluded the evaluation model changes had the effect of moving the PCT location one grid span from the ruptured location rather than having the PCT occur in the node adjacent to the ruptured node. They compared cladding temperatures in the next grid span for the cases with discharge coefficients of 1.0, 0.8, and 0.6. Although the range of cladding temperatures in the next grid span was smaller, the discharge coefficient of 1.0 case still had the highest PCT. Because the case with the discharge coefficient of 1.0 had the highest PCT in both the ruptured grid span and the adjacent grid span, that case was chosen for the next step in the sensitivity study.

### 3.3 Break Type

The next study determined whether the guillotine or split break type was most limiting. A split break with an area equal to twice the cold leg pipe size and discharge coefficient of 1.0 was analyzed and compared to the corresponding guillotine break. The guillotine break had the highest PCT. In response to question 12, Reference 6, it was stated the guillotine break is the worst break type because during reflood it allows steam to flow out an area equivalent to one-half the break area. For a split break, the effective area for steam flow from the vessel is less than half the total break area. This allows more steam to vent from the downcomer for the guillotine break, reducing the downcomer pressure, and lowering the core reflood rate. Thus, a higher PCT is calculated for the guillotine break.

To verify the results of the study apply to BWFC's final version of the EM, Duke Power Company compared the cladding temperatures in the grid span above the one with the rupture location. The guillotine break also had the highest cladding temperature in this grid span. Therefore, it was chosen for the next step in the study.

### 3.4 Maximum ECCS Flow Analysis

To determine the single failure that results in the highest PCT, Duke Power Company compared the results of analyses that used the maximum and

minimum ECCS flows. Related to the discussion at the beginning of Section 3 of this report on the differences between the Westinghouse EM and BWFC EM PCT results, the minimum pumped injection flow curves used in the study were the smallest of the four units and the maximum pumped injection flow curves used in the study were the largest of the four units. The maximum ECCS flow case resulted in the highest PCT and was chosen for use in the LOCA limits analyses.

To verify the results of the study apply to BWFC's final version of the EM, Duke Power Company compared the cladding temperatures in the grid span above the one with the rupture location. The maximum ECCS flow case also had the highest cladding temperature in this grid span. Therefore, it was chosen for use in the LOCA limits analysis.

### 3.5 Summary

As a result of the studies performed, the following boundary conditions were chosen for the LOCA limits analysis: Catawba accumulator configuration, guillotine break with discharge coefficient of 1.0, and the maximum ECCS flow. Based on the fact the conditions that resulted in the highest PCT were chosen, INEL considers the conditions chosen appropriate for use in the Duke Power Company LOCA limits analysis.

#### 4. COMPLIANCE WITH 10 CFR 50.46

##### 4.1 Large-Break LOCAs

###### 4.1.1 Temperature and Oxidation Criteria

Compliance with the temperature and oxidation criteria of 10 CFR 50.46 for LBLOCAs was determined by a direct application of the BWFC RSG LOCA EM. The analyses were performed for a guillotine break with discharge coefficient of 1.0 and the maximum ECCS flow. The total core power was 3479 MW<sub>t</sub>, 102% of 3411 MW<sub>t</sub>. The large-break LOCA limit calculations were run with a total peaking factor ( $F_Q$ ) of 2.32 at all elevations up to 8.0 ft. Above 8.0 ft, the peaking was reduced linearly until  $F_Q$  was approximately 2.1 at the 12 ft elevation (see Figure B-1, Reference 2). Duke Power Company also imposed a burnup limit on  $F_Q$  in the form of a multiplier that was  $\leq 1.0$ . This was done to ensure the EOL conditions would not result in a blowdown rupture. The multiplier was 1.0 up to a burnup of approximately 50,000 MWD/Mtu. For burnups greater than 50,000 MWD/Mtu, the multiplier reduced linearly until it was approximately 0.85 at a burnup of 60,000 MWD/Mtu (see Figure B-2, Reference 2).

Five axial power shapes, with peaks at 2.9, 4.6, 6.3, 8.0, and 9.7 ft were studied. The limiting axial power shape was the case with the 4.6 ft peak. For this case, the PCT was 1945°F (Reference 7), the peak local oxidation was 4.9%, and the whole core oxidation was 0.55% (question 13, Reference 5). All these values are well below 10 CFR 50.46 acceptance criteria of PCT less than 2200°F, local oxidation less than 17%, and whole core oxidation less than 1%. The response to question 15, Reference 8, showed the uncertainty in PCT due to the possibility of the axial power shape in the plant being different from the ones analyzed was less than 50°F. Therefore, within the limits allowed by 10 CFR 50.46, the first three 10 CFR 50.46 acceptance criteria are met.

In the LBLOCA limits analyses, the cold leg temperature is set to a nominal cold leg temperature based on the core flow and the control system response to the core flow (Table 9-2, Reference 1). The core flow used in the LBLOCA limits cases was the flow determined from the at power



departure from nucleate boiling (DNB) analysis. Because the calculated PCT is sensitive to the cold leg temperature used in the analysis, additional analyses may need to be performed for one of the Catawba or McGuire units if it is operated outside the normal operating mode. For example, if Duke Power Company extends the operating cycle for one of the units and in doing so the cold leg temperature decreases to below the analyzed temperature, additional analyses are needed to support this operation because it is outside the normal mode of operation.

#### 4.1.2 Coolable Geometry Criterion

The fourth criterion of 10 CFR 50.46 requires the core remain in a coolable geometry when both thermal and mechanical loads are considered. Duke Power Company noted the mechanical loads affect only the outer two or three points in the lattice structure of the core. Because those fuel rods operated at relatively low power, they did not rupture during the LOCA analyses. Additional information, provided in Duke Power Company's response to question 19, Reference 5, demonstrated the average core temperature remained below the rupture temperature. Therefore, Duke Power Company concluded, and the INEL agrees, the thermal and mechanical effects can be considered separately.

Duke Power Company noted the hottest bundle in the core was able to cool successfully and maintain a PCT below 2200°F. The analyses included the effects of cladding deformation and rupture (the maximum hot channel blockage calculated for Catawba and McGuire was 60%). Duke Power Company argued that because rupture locations on different rods are not coplanar but distributed within the upper part of a grid span, the bundle retains a coolable rod-coolant channel-rod arrangement. The INEL reviewed data on tests with cladding rupture<sup>26,27</sup> and found that it supported Duke Power Company's argument on non-coplanar rupture.

With respect to the effects of rod bowing and fuel assembly damage from external forces (pipe breaks and seismic events), the NRC reviewed the adequacy of the BWFC Mark-BW fuel design in Reference 28. The NRC found the Mark-BW design adequately accounted for rod bowing and maintained a coolable geometry when subjected to a combined LOCA and seismic load.

Based on the above considerations, Duke Power Company has adequately demonstrated the cores for Catawba and McGuire retain a coolable geometry following a LOCA.

#### 4.1.3 Long Term Cooling Criterion

Criterion five of 10 CFR 50.46 requires the long term cooling of the core be demonstrated after the initial success of the ECC system. Duke Power Company noted the initial, successful operation of the ECCS is demonstrated by the LOCA limits analyses. Those analyses demonstrated the core would be cooled and the core returned to low cladding temperatures. They also noted that once the cladding has returned to low temperatures, maintaining the low temperature condition requires being able to provide a continuous supply of coolant to the core. Duke Power Company stated the capability to provide long term coolant supply to the core is demonstrated in the Catawba and McGuire safety analysis reports (SARs) and is not dependent on the fuel design; therefore, they concluded the previous licensing base remained valid with Mark-BW reload fuel. The INEL agrees with Duke Power Company's conclusion that the previous licensing base remains valid for the Mark-BW fuel because it is independent of fuel design. Also, fuel design was the only change for Catawba and McGuire proposed by Reference 2; that is, no other system design or technical specification changes were proposed in addition to the Mark-BW reload fuel.

The final long-term cooling consideration is preventing boric acid precipitation in the core for cold leg breaks. In Reference 2, Duke Power Company noted this was accomplished by establishing hot leg recirculation core cooling within 15 hours of accident initiation. Demonstrating the sufficiency of the 15 hour limit is dependent only on showing boric acid concentrations remain below solubility limits. In turn, this depends only on injection rate, reactor coolant system (RCS) geometry, and core power level. That is, it is independent of fuel design. In Reference 2, Duke Power Company stated that, because none of the above factors changed, they concluded the previous evaluation for boric acid precipitation remained valid with Mark-BW reload fuel. However, the INEL noted in a conference call with Duke Power Company on September 26, 1990 that, because of the larger diameter of the Mark-BW fuel rod, the core volume is reduced by

approximately 38 ft<sup>3</sup> with Mark-BW reload fuel. In a subsequent conference call on October 8, 1990, it was noted the boric acid precipitation time was recalculated by Duke Power Company and the time to hot leg recirculation was now set at nine hours. During a call on November 5, 1990, it was indicated this calculation would be provided with the Catawba and McGuire updated SARs in 1991 and the operating procedures when the plants restart with BWFC reload fuel. Review of this calculation will need to be completed at that time.

Based on the above findings, the INEL concludes Duke Power Company has adequately addressed the NRC concerns stated in the long-term cooling criterion of 10 CFR 50.46.

#### 4.1.4 Large-Break LOCA Summary

The limiting LBLOCA was a guillotine break with discharge coefficient of 1.0 and the maximum ECCS flow. The limiting axial power shape was the core with the 4.6 ft peak and total core power was 3479 MW<sub>t</sub>, 102% of 3411 MW<sub>t</sub>. For this case, the PCT was 1945°F, the peak local oxidation was 4.9%, and the whole core oxidation was 0.55%. Therefore for LBLOCAs, Duke Power Company provided analyses and sufficient information to demonstrate:

1. Peak cladding temperatures were less than 2200°F.
2. Maximum local cladding oxidation was less than 17%.
3. Core wide oxidation was less than 1%.
4. The core geometry remains amenable to cooling.
5. Long-term cooling was assured.

#### 4.2 Small-Break LOCAs

Duke Power Company did not use the BWFC RSG LOCA EM to reanalyze the small-break spectrum for Catawba and McGuire with Mark-BW reload fuel to show compliance with the acceptance criteria of 10 CFR 50.46 for SBLOCAs. Rather, Duke Power Company provided information to justify the previous licensing base remained valid for the plants with Mark-BW fuel. A summary of Duke Power Company's justification and the INEL evaluation follows.

#### 4.2.1 Duke Power Company Justification

First, Duke Power Company noted previous SBLOCA analyses were not limiting for Catawba and McGuire. Second, the differences between Mark-BW fuel and the Westinghouse fuel are small. Fuel assembly differences were discussed by Duke Power Company in detail (see below).

Westinghouse and BWFC fuel assemblies differ in the following areas: assembly unrecoverable pressure drop, initial fuel temperatures, initial internal rod pressures, the axial power shape, and fuel rod diameter. The effect of each of these areas was evaluated by Duke Power Company, and their justification that the effect of these areas on Catawba's and McGuire's SBLOCA response is small is discussed below.

The difference in assembly pressure drops is less than 1 psi, with the Mark-BW assembly having the smaller pressure drop. The effect of this difference on the overall loop flow would be less than 1%. Because the flow and also the core power are the same as in previous analyses, the initial hot leg temperatures for Catawba and McGuire with BWFC reload fuel will be less than 1°F different from the hot leg temperatures with Westinghouse fuel. Therefore, the subcooled depressurization phase of a SBLOCA will be the same regardless of the fuel, and the reactor and pump trip signals will occur at the same time in the transient.

During the flow coastdown and natural circulation phase of the accident, the flow rates are smaller relative to the steady state flow. Because the difference in assembly pressure drop is small, the effect of the pressure drop difference will be even less during this phase of the accident than during the subcooled depressurization phase. Duke Power Company noted the flow during this phase of the SBLOCA is sufficient to prevent critical heat flux and transfer all the initial stored energy to the steam generators.

Differences in initial fuel temperature add or subtract overall energy from the RCS. Information was provided in Reference 2 to show the initial fuel energy is removed from the fuel rod during the pump coastdown phase; therefore: (a) this difference has no impact beyond the pump coastdown

phase and (b) the core energy content during the loop draining and boiloff phases will be identical to the current licensing calculations based on Westinghouse fuel. Additional information was provided by Duke Power Company in Reference 5, response to question 20. This response stated that once the reactor trips, the energy in the rods decreases and establishes the temperature distribution needed to remove decay heat with an average temperature close to the saturation temperature. In a telecon with Duke Power Company on September 25, 1990, the statement that the core energy content during the loop draining and boiloff phases would be identical to the current licensing calculations based on Westinghouse fuel was revised to state that the core energy content would be similar. Also, the response to question 20 points out it is the decay heat during the boiloff phase which will determine the PCT not the stored energy in the core. Catawba and McGuire will be operated at the same power level with Mark-BW fuel as with OFA fuel; therefore, the decay heat for the two fuel types will be the same. Because the mixture level is also expected to be similar for the two fuel types during a SBLOCA (question 23, Reference 5), Duke Power Company concluded the impact of differences in initial stored energy on PCT is minimal.

Fuel rod internal pressures for the two fuel types differ only slightly. The difference in internal pressure could affect the rod gap dimensions and the time to rupture. Prior to core uncover when fuel temperatures are low, the gap differences will be negligible. Duke Power Company also noted that the internal pressure change due to burnup of the two fuel types is such that the Mark-BW fuel internal pressure will increase more slowly relative to the Westinghouse OFA fuel. As a result, by the time the fuel burnup reaches a point where rupture is possible during a SBLOCA, the OFA fuel rod pressure is higher and the OFA fuel would tend to rupture sooner than the Mark-BW fuel. However, because the SBLOCA PCT is approximately 1500°, the impact of a difference in rupture timing would be negligible.

Finally, Duke Power Company noted the SBLOCA imposed plant operating limits, including maximum allowable total peaking, are not being changed when BWFC fuel is introduced into the core. Therefore: (a) the axial power profile used by Westinghouse in the SBLOCA analyses remains bounding.



(b) the thermal load imposed on the fuel during a temperature excursion is conservative, and (c) the PCT for the current SAR evaluations are conservative for the Mark-BW fuel. Additional information provided by Duke Power Company noted that the axial power shape depends more on plant design and operation rather than fuel assembly design (question 21, Reference 4). Also, the power shape chosen for evaluation in the SBLOCA analysis is done to ensure a bounding evaluation is performed rather than anticipating that any fuel assembly would experience that axial profile. Also, the Westinghouse axial power shape, which is the same axial power shape that Duke Power Company would use in a direct evaluation of SBLOCAs with Mark-BW fuel, will be used as the bounding power shape for the Mark-BW fuel in plant operating limits.

In response to question 22, Reference 5, Duke Power Company noted that they reviewed the ECCS pump flows, ECC water temperatures, and boron concentrations in the existing SBLOCA FSAR analyses. They stated any technical specification changes needed to ensure plant operation consistent with the analysis assumptions will be proposed for the first Catawba and McGuire reload with Mark-BW fuel. Current SBLOCA analyses also assume 10% steam generator tube plugging for both plants. The loop resistances used in current FSAR SBLOCA analyses was reviewed and approved and there have been no changes in the system flow or total loop differential pressure since that analysis. Also, loop resistance will not be altered by the change in fuel type.

Based on the information above, Duke Power Company concluded the current small-break analyses in the Catawba and McGuire SARs remain valid as a licensing base even after Mark-BW reload fuel is introduced into the cores.

#### 4.2.2 INEL Evaluation

The LBLOCAs at Catawba and McGuire are clearly more limiting. Based on updated SARs for the plants,<sup>29,30</sup> the PCT for Catawba are 1703.7°F for the LBLOCA and 1304°F for the SBLOCA. For McGuire, the PCTs are 1841°F for the LBLOCA and 1488°F for the SBLOCA. For Catawba the 4-inch break was limiting, while the 3-inch break was limiting for

McGuire. This comparison confirms Duke Power Company's statement that, based on past analyses, the LBLOCA is limiting to Catawba and McGuire.

Regarding the difference in assembly pressure drop, INEL concurs with Duke Power Company's judgement that the effect should be small. With the end result being less than a 1°F difference in hot leg temperature, the initial subcooled blowdown of the plants with Mark-BW reload fuel should not be any different than with Westinghouse fuel. The INEL also agrees that, because of the small differences in assembly pressure drop, differences during the pump coastdown and natural circulation phase would also be negligible.

As discussed in the previous section, information was provided by Duke Power Company to show the initial fuel energy is removed from the fuel rod during the pump coastdown phase. The INEL agrees that the stored energy will be similar rather than identical. This is because differences in rod dimensions for the fuel pellets and cladding would result in differences in stored energy even with the same decay heat temperature distribution for the two fuel types. The INEL also agrees that equality of decay heat, not equality of stored energy, is the important factor in determining the PCT. Therefore, the difference in initial stored energy is not significant during the core uncover phase.

Because the fuel rod internal pressures for the two fuel types differ only slightly, it was argued in Reference 2 that the effect on SBLOCAs should be negligible. Duke Power Company also stated that the OFA rod should rupture slightly earlier than the Mark-BW rod in SBLOCAs that occur from fuel burnups where rupture is possible. The INEL reviewed the Catawba and McGuire<sup>31</sup> FSARs and notes the Westinghouse SBLOCA analyses did not calculate any fuel rods to rupture. Based on the information supplied by Duke Power Company and the information in the Catawba and McGuire FSARs, the INEL concludes that Mark-BW fuel also would not rupture in an SBLOCA analysis based on current Westinghouse calculated hydraulic conditions and SBLOCA assumptions (e.g., fuel burnup, initial power, steam generator tube plugging, ECCS flows, etc.). Therefore, the INEL agrees that the effect on SBLOCA analyses will be negligible. This includes the effect on gap dimensions and rupture.

Finally, INEL agrees with Duke Power Company's statement that the actual power shape imposed on the fuel is more dependent on plant design and operation rather than fuel assembly design. Also, because the operating limits for the Mark-BW fuel will be based on the power shape used to evaluate OFA fuel and the same axial power shape would have been used by Duke Power Company to evaluate SBLOCAs directly, the INEL concluded sufficient information was provided to justify that the axial power profile used by Westinghouse in the SBLOCA analyses remains bounding and the PCT for the current SAR evaluations are conservative for the Mark-BW fuel.

The information provided by Duke Power Company in response to question 22, Reference 5, regarding the analysis assumptions in the current FSAR SBLOCA analyses and the current operating status of the plants was also reviewed. Because Duke Power Company essentially stated that the plants would be operated within the bounds of the current licensing analysis base, that response is considered acceptable.

#### 4.2.3 Small-Break LOCA Summary

Based on the above information, the INEL recommends the current SBLOCA licensing base for Catawba and McGuire be used to license the plants with BWFC reload fuel. Based on the previous Westinghouse analyses, the limiting PCT for Catawba is 1304° (4-inch break) and 1488°F for McGuire (3-inch break). However, should Duke Power Company modify Catawba or McGuire so that the FSAR SBLOCA analysis assumptions (e.g., ECCS flows, steam generator tube plugging, etc.) no longer remain valid, SBLOCA results based on the BWFC RSG LOCA EM will need to be provided to the NRC for review. Also, the INEL notes that should Duke Power Company reanalyze SBLOCAs for Catawba and McGuire, the entire SBLOCA spectrum will need to be reanalyzed to identify the worst break size and location (i.e., severed injection line and breaks at the bottom and top of the cold leg pipe which could be sensitive to loop seal phenomena) with the BWFC RSG LOCA EM.

#### 4.3 Mixed Core Operation

Because Catawba and McGuire are currently loaded with a full core of Westinghouse fuel, the plants will operate several cycles with mixed cores.



i.e., cores containing both Westinghouse and BWFC Mark-BW fuel. To support operation of Catawba and McGuire with the mixed core, Duke Power Company discussed the effects of mixed core operation as they relate to LBLOCAs and SBLOCAs at the plants. The effects of a mixed core on LBLOCAs are discussed first followed by SBLOCAs.

#### 4.3.1 Mixed Core Operation - LBLOCAs

Duke Power Company identified the following differences between the Westinghouse and BWFC fuel assemblies: guide tube and instrument tube differences (the upper portion of the guide tube and the whole instrument tube is larger for Mark-BW fuel); fuel and cladding dimensions (the Mark-BW fuel rod is larger than the Westinghouse fuel, including fuel, gap, and cladding dimensions); and fuel assembly pressure drop (the pressure drop for the Mark-BW assembly is slightly lower than the Westinghouse assembly). To determine the effect of these differences on mixed core operation, Duke Power Company analyzed two cases, one with a Mark-BW assembly as the hot assembly and Westinghouse fuel in the average core and one with a Westinghouse assembly as the hot assembly and Mark-BW fuel in the average core.

During blowdown for the case with the Mark-BW hot assembly, Duke Power Company stated flow diversion from the Westinghouse assemblies to the Mark-BW assembly will decrease the end-of-blowdown temperature by 30 to 50°F. However, during reflood, the effect on PCT is in the opposite direction due to the higher core pressure drop of the Westinghouse fuel. Relative to a core with only Mark-BW assemblies, the core refloods slower and the PCT for the Mark-BW assembly increases by approximately 30°F. The overall effect is a PCT less than 50°F different from the case with a full Mark-BW core. Because the PCT calculated with the BWFC EM for a full Mark-BW core is 1945°F, the PCT for a Mark-BW fuel assembly during any period of mixed core operation will also be less than the 10 CFR 50.46 acceptance criterion of 2200°F.

For the case with the Westinghouse fuel as the hot assembly, the effects are the reverse of those discussed above for the Mark-BW assembly, but the overall change is still small. During blowdown, flow diversion

from the Westinghouse fuel assembly to the Mark-BW assemblies will cause the end-of-blowdown temperature of the Westinghouse fuel to increase 30 to 50°F. However, during reflood, the core will reflood faster than a core with only Westinghouse fuel assemblies and the PCT will be reduced by approximately 30°F. The overall effect is a PCT less than 50°F different from the case with a full Westinghouse core. Because the PCT calculated by Westinghouse for a full Westinghouse core is 1704°F at Catawba and 1841°F at McGuire, the PCT for a Westinghouse fuel assembly during any period of mixed core operation will also be less than the 10 CFR 50.46 acceptance criterion of 2200°F.

For mixed core operation, Duke Power Company showed the LBLOCA PCT for the BWFC fuel may decrease slightly from the 1945°F calculated with the BWFC RSG LOCA EM and may increase slightly from the 1704°F and 1841°F calculated by Westinghouse for Westinghouse fuel in Catawba and McGuire. However, the increase for Catawba and McGuire will be small enough that the 1945°F calculated for BWFC reload fuel is still limiting.

Based on the above considerations, INEL concludes Duke Power Company has adequately demonstrated the LBLOCA cladding temperatures will remain acceptably low during the transition period with mixed core operation.

#### 4.3.2 Mixed Core Operation - SBLOCAs

Duke Power Company addressed the effects of mixed core operation on SBLOCAs in response to questions 23 and 24, References 5 and 6. The differences addressed include unrecoverable pressure drop, initial fuel temperature, initial fuel rod internal pressure, and the axial profile (question 24) plus the effect of the larger Mark-BW rod diameter on core levels (question 23).

Concerning the difference in unrecoverable pressure drop, Duke Power Company noted that the pressure drop difference is small and occurs at the assembly entrance. Because the entrance is covered with water during the core boiloff and the flow rates are so low, any flow related pressure changes are inconsequential with respect to mixed core operation. The INEL agrees with this conclusion.

Duke Power Company noted the Mark-BW assembly will have an initial stored energy content approximately 7% greater than the Westinghouse OFA. The mechanisms to remove the differences in initial stored energy were discussed in the response to question 20, Reference 5. That response showed the differences in stored energy would easily be removed during the flow coastdown period of the SBLOCA and any differences at the beginning of the core boiloff would be minimal. Therefore, there will be no adverse consequences from the difference in initial stored energy during mixed core operation. The INEL agrees with this conclusion and does not see a mechanism by which the difference in stored energy would affect mixed core operation.

Duke Power Company noted the difference in the fuel rod gas internal pressure could impact the timing of rupture slightly, but the impact of a rupture or rupture timing difference would be negligible. Rupture does not affect vessel inventory or cause interactions between the two fuel designs. Duke Power Company in Reference 2, also stated that for SBLOCAs that occur from fuel burnups where rupture is possible the OFA fuel would tend to rupture earlier than the Mark-BW fuel. Therefore, the INEL agrees that the effect on SBLOCA analyses with mixed cores is negligible.

For the axial power profile, Duke Power Company noted the same axial power profile is used in the SBLOCA evaluation of OFA fuel by Westinghouse and Mark-BW fuel by BWFC. Therefore, the potential difference in assembly power shapes not approaching allowable limits will not have an adverse effect on mixed core operation. The INEL concurs.

Finally, Duke Power Company noted that the geometrical differences between assemblies could have a slightly positive effect on mixed core operation because of a higher minimum core level. Because the OFA calculations are the calculations of record for the Mark-BW fuel, Duke Power Company concluded there is not an adverse effect on mixed core operation. After reviewing the information from Duke Power Company, the INEL agrees.

Duke Power Company showed mixed core operation should not have an impact on SBLOCAs. Therefore, based on the Westinghouse SBLOCA analyses,

the SBLOCA mixed core PCTs should remain 1304°F and 1488°F for Catawba and McGuire, respectively.

Based on the above considerations, INEL concludes Duke Power Company has adequately demonstrated the SBLOCA cladding temperatures will remain acceptably low during the transition period with mixed core operation.

#### 4.3.3 Mixed Core Summary

For mixed core operation, Duke Power Company showed LBLOCAs remain limiting for Catawba and McGuire. The mixed core PCT for Catawba and McGuire during a LBLOCA should be less than or equal to 1945°F but greater than or equal to 1841°F which exceeds the SBLOCA mixed core PCTs of 1488°F and 1304°F for McGuire and Catawba, respectively.

For mixed core operation, Duke Power Company stated (Appendix A, Reference 2) technical specifications for each type of fuel assembly will be based on the full core analyses for each fuel type. That is, technical specifications for the Mark-BW fuel will be based on Reference 2 analyses, and the technical specifications for the OFA fuel will be based on FSAR analyses. Also, operational limits or technical specifications not directly applied to a fuel assembly will be based on the analysis with generated the most stringent limit.

#### 4.4 Overall LOCA Summary

The above analysis results and information show that Catawba and McGuire will meet 10 CFR 50.46 criteria with BWFC reload fuel. For LBLOCAs, SBLOCAs, and mixed core operation, Duke Power Company showed LBLOCAs are limiting. The limiting break was a cold leg guillotine break with a discharge coefficient of 1.0 and an axial peak at 4.6 ft. Total core power was 3479 MW<sub>t</sub>, 102% of 3411 MW<sub>t</sub>. For this break with a full core of BWFC reload fuel, the PCT was 1945°F, peak local oxidation was 4.9%, and the peak whole core oxidation was 0.55%. All these values are below the criteria of 10 CFR 50.46.

## 5. CONCLUSIONS

The information provided by Duke Power Company in BAW-10174, Rev. 1, Mark-BW Reload LOCA Analysis for the Catawba and McGuire Units, and in their responses to NRC questions was reviewed to determine whether the acceptance criteria of 10 CFR 50.46 were met for the Catawba and McGuire plants with Mark-BW reload fuel. Based on our review, the INEL reached the following conclusions and recommends BAW-10174, Rev. 1, be accepted to support operation of the Catawba and McGuire units with BWFC reload fuel provided the conditions identified below are met.

1. For LBLOCAs, the limiting break was a guillotine break with discharge coefficient of 1.0 and the maximum ECCS flow. The total core power was 3479 MW<sub>t</sub>, 102% of 3411 MW<sub>t</sub>. The maximum total peaking factor ( $F_Q$ ) was 2.32 at elevations up to 8.0 ft. Above 8.0 ft, the peaking was reduced linearly until  $F_Q$  was approximately 2.1 at the 12 ft elevation. The PCT was calculated for the case with the axial peak at 4.6 ft. The BWFC RSG LOCA EM was used in these analyses. Therefore, Duke Power Company provided analyses and sufficient information to demonstrate the acceptance criteria of 10 CFR 50.46 were met:
  1. Peak cladding temperatures were less than 2200°F (1945°F).
  2. Maximum local cladding oxidation was less than 17% (4.9%).
  3. Core wide oxidation was less than 1% (0.55%).
  4. The core geometry remains amenable to cooling.
  5. Long-term cooling was assured.
2. For SBLOCAs, Duke Power Company did not use the BWFC RSG LOCA EM to reanalyze the small-break spectrum. Rather, they provided sufficient information to demonstrate the current SBLOCA licensing base for Catawba and McGuire is adequate to license those plants with Mark-BW reload fuel. Based on the previous analyses, the SBLOCA PCTs for Catawba and McGuire are 1304°F (4-inch break) and 1488°F (3-inch break), respectively. Therefore, 10 CFR 50.46 acceptance criteria were met for SBLOCAs.
3. Based on the above, LBLOCAs are limiting at Catawba and McGuire.

4. For a full Mark-BW core, the calculated PCT was 1945°F, and for a full OFA core, the calculated PCTs were 1704°F and 1841°F for Catawba and McGuire, respectively. During mixed core operation, Duke Power Company showed the calculated PCT would be bounded by 1945°F as calculated for a full Mark-BW core during a large-break LOCA. Therefore, the acceptance criteria of 10 CFR 50.46 will be met for Catawba and McGuire during the period of mixed core operation when both Westinghouse OFAs and BWFC Mark-BW fuel assemblies will reside in the core.
5. The methods used to perform the LOCA analyses for Catawba and McGuire were approved by the NRC, and the methods were applied in a manner consistent with any NRC conditions of acceptance.
6. Because of the larger diameter of the Mark-BW fuel rod, the core volume is reduced by approximately 38 ft<sup>3</sup> with Mark-BW reload fuel. In a conference call on October 8, 1990, Duke Power Company noted the boric acid precipitation time was recalculated and the time to hot leg recirculation was now set at nine hours. During a call on November 5, 1990, it was indicated this calculation would be provided with the Catawba and McGuire updated SARs in 1991 and the operating procedures when the plants restart with BWFC reload fuel. Review of this calculation will need to be completed at that time.
7. Information on the fuel temperature and moderator void reactivity void coefficients used in the LOCA analyses was provided by Duke Power Company. The Doppler temperature coefficient used was -2.2 pcm/°F. The moderator density reactivity is shown in a curve, provided in Duke Power Company's response to question 2, Reference 4, that varied the moderator density coefficient from 0.0 %delta-k/k at a relative density of 1.0 to -20.854 %delta-k/k at a relative density of 0.2. The plants should be operated within the bounds set by the above analysis limits for the Doppler and Moderator density coefficients.



8. As discussed in Section 4.2 of this report, Duke Power Company provided sufficient information to justify the current licensing base for SBLOCAs was still applicable to Catawba and McGuire with BWFC reload fuel. However, should Duke Power Company modify Catawba or McGuire so that the FSAR SBLOCA analysis assumptions (e.g., ECCS flows, steam generator tube plugging, etc.) no longer remain valid, SBLOCA analysis results based on the BWFC RSG LOCA EM will need to be provided to the NRC for review. Also, the INEL notes that should Duke Power Company reanalyze SBLOCAs for Catawba and McGuire, the entire SBLOCA spectrum will need to be reanalyzed to identify the worst break size and location (i.e., severed injection line and breaks at the bottom and top of the cold leg pipe which could be sensitive to loop seal phenomena) with the BWFC RSG LOCA EM.
9. In the LBLOCA limits analyses, the cold leg temperature is set to a nominal cold leg temperature based on the core flow and the control system response to the core flow. The core flow used in the LBLOCA limits cases was the flow determined from the at power DNB analysis. Because the calculated PCT is sensitive to the cold leg temperature used in the analysis, additional analyses may need to be performed for one of the Catawba or McGuire units if it is operated outside the normal operating mode. For example, if Duke Power Company extends the operating cycle for one of the units and in doing so the cold leg temperature decreases to below the analyzed temperature, additional analyses are needed to support this operation because it is outside the normal mode of operation.
10. For mixed core operation, Duke Power Company stated technical specifications for each type of fuel assembly will be based on the full core analyses for each fuel type. That is, technical specifications for the Mark-BW fuel will be based on Reference 2 analyses, and the technical specifications for the OFA fuel will be based on FSAR analyses. Also, operational limits or technical specifications not directly applied to a fuel assembly will be based on the analysis with generated the most stringent limit.

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Mark-BW Reload LOCA Analysis  
for the  
Catawba and McGuire Units

Key Words: Large Break, LOCA, Transient, Water Reactors

ABSTRACT

The B&W Fuel Company will be delivering reload fuel to the Duke Power Catawba and McGuire Units beginning in 1991. This report presents a complete LOCA evaluation for operation of the Catawba and McGuire nuclear units with Mark-BW reload fuel. Compliance with the criteria of 10 CFR 50.46 is demonstrated. Operation of the units while in transition from Westinghouse-supplied OFA fuel to B&W-supplied Mark-BW fuel is also justified. Other B&W topical reports describe the Mark-BW fuel assembly design; the mechanical, nuclear, and thermal-hydraulics methods supporting the design; and ECCS codes and methods. The analyses and evaluations presented in this report serve, in conjunction with the other topical reports, as a reference for future reload safety evaluations applicable to cores with BWFC-supplied fuel assemblies.

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### Documentation

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1	Add Appendix B--increased 9.7' case $F_0$ . Correct x-scale on Chapter 8 mass flux plots. NRC requested updates.
1	Original issue of Accepted version. Add Appendix C.

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## 1.0 Introduction

The B&W Fuel Company (BWFC) will be delivering reload fuel to the Duke Power Catawba and McGuire units beginning in 1991. The Mark-BW reload fuel will be similar in design and performance to fuel assemblies already licensed and operating in these plants. In accordance with the requirements of 10 CFR 50.46 and 10 CFR 50, Appendix K, an evaluation of the Emergency Core Cooling System (ECCS) performance has been performed for BWFC reload fuel for the Catawba and McGuire units. In presenting that analysis, this report complements other BWFC topical reports that describe the Mark-BW fuel design; the mechanical, nuclear, and thermal-hydraulics methods supporting the design; and ECCS codes and methods. The analyses and evaluations presented in this report are intended to serve, in conjunction with these other topical reports, as a reference for future reload safety evaluations of the Catawba and McGuire units applicable to cores with BWFC-supplied fuel assemblies.

The McGuire and Catawba nuclear power plants use nuclear steam supply systems designed by Westinghouse that are representative of the standard Westinghouse four-loop, 3411 Mwt design, with the exception of certain internal structures. The ECCS provided for the plant consists of the conventional combination of high pressure pumped injection, pressurized water storage tanks, and low pressure pumped injection all connected into the reactor coolant piping just upstream of the reactor vessel. The McGuire and Catawba containments are of the ice condenser type, limiting building pressures during a postulated loss of coolant accident (LOCA) to near atmospheric values.

The results of calculated predictions of LOCAs must meet the criteria imposed by 10 CFR 50.46. At the time of initial operation, the McGuire and Catawba plants were fueled with Westinghouse-supplied fuel and compliance was demonstrated by



calculations performed by Westinghouse and Duke Power. This report documents compliance to 10 CFR 50.46 when the plants are fueled by BWFC-supplied fuel. In this report, possible LOCAs are divided into two groups depending on the assumed break size. For breaks larger than 1.0 ft<sup>2</sup>, compliance is demonstrated by calculations and analyses performed in accordance with the BWFC large break LOCA evaluation model for recirculating steam generator plants, BAW-10168 (Reference 1). For breaks smaller than 1.0 ft<sup>2</sup>, compliance is shown by validating that the calculations performed in support of the plant prior to the loading of the Mark-BW fuel remain applicable when the Mark-BW is in use.

A summary of the results of the analyses is presented in Chapter 2. Chapter 3 provides a general description of the Catawba and McGuire plants including their major differences. The analysis parameters used for the large break calculations are discussed in Chapter 4, evaluation model changes incorporated for this analysis are discussed in Chapter 5, and system sensitivity studies in Chapter 6. The large break spectrum analysis to determine the most limiting break is documented in Chapter 7. Chapter 8 presents the LOCA limit calculations which confirm adherence to the first two criteria of 10 CFR 50.46. The evaluation of maximum hydrogen generation, coolable geometry, and long-term cooling are presented in Chapters 9, 10 and 11, respectively. Validation of the applicability of the earlier small break LOCA studies is provided in Chapter 12.

During the transition from Westinghouse fuel to the Mark-BW assembly, the core will for some time consist of a mix of the two fuel assembly types. For such cycles, Appendix A shows that the mixing of the assemblies does not alter the LOCA performance of either fuel assembly to any degree approaching the criteria of 10 CFR 50.46.

## 2.0 Summary and Conclusion

10 CFR 50.46 specifies that the emergency core cooling system (ECCS) for a commercial nuclear power plant must meet five criteria. The calculations and evaluations documented in this report demonstrate that the Duke Power Company McGuire and Catawba units continue to meet these criteria when operated with Mark-BW fuel. Large break LOCA calculations performed in concurrence with an approved evaluation model (BAW-10168 and revisions) demonstrate compliance for a full Mark-BW core for breaks up to and including the double-ended severance of the largest primary coolant pipe. The small break LOCA calculations used to license the plant operation during previous fuel cycles are shown to be unaffected by the change in fuel design, and therefore, demonstrate that the plant meets 10 CFR 50.46 for small breaks when loaded with Mark-BW fuel. The coexistence of the Mark-BW assembly and the Westinghouse OFA assembly in the same fuel cycle is shown to be inconsequential and does not cause the calculated temperatures for either assembly to approach the limits of 10 CFR 50.46.

Specifically, this report demonstrates that when the McGuire and Catawba plants are operated with Mark-BW fuel:

1. The calculated peak cladding temperatures for the limiting cases are less than 2200 F, (Chapter 8).
2. The maximum calculated local cladding oxidation is less than 17.0 %, (Chapter 8).
3. The maximum amount of core-wide oxidation does not exceed 1.0 % of the fuel cladding, (Chapter 9).
4. The cladding remains amenable to cooling, (Chapter 10).

5. Long-term cooling is established and maintained after the LOCA, (Chapter 11).

The results of the large break sensitivity studies and the break spectrum studies performed with the BWFC evaluation model show that a double-ended guillotine break at the pump discharge with a discharge coefficient of 1.0 and maximum ECCS is the most limiting case. Table 2-1 shows the results of this accident on the Mark-BW fuel design when the assumed axial location of peak power is varied along the length of the pin. As the local power for the Mark-BW assemblies is controlled such that it can not exceed the Mark-BW LOCA limits curve, this table lists the results of the calculations which demonstrate compliance with the first four criteria of 10 CFR 50.46. Compliance with the long-term cooling criterion (as described in Chapter 11), is through the use of a pumped injection system that can be recirculated, drawing water from the reactor building through a heat exchanger, to provide extended energy removal. The concentration of boric acid is held below its solubility limit by starting hot leg injection with 15 hours of the accident.

During the transition from the Westinghouse OFA to the Mark-BW, both fuel assemblies will reside in the core simultaneously for several fuel cycles. Appendix A demonstrates that the results and conclusions presented above are also applicable to the Mark-BW assemblies in the transition core. Similarly, Appendix A demonstrates that the insertion of the Mark-BW fuel with the OFA does not adversely affect the cooling of the OFA. Thus, the original calculations that showed that the OFA met the criteria of 10 CFR 50.46 remain valid for the OFA assemblies through the transition period.

Table 2-1 Summary of Results (LOCA Limit Runs)

<u>Core Elevation, ft</u>	<u>Peak Cladding Temperature, F<sup>1</sup></u>	<u>Maximum Oxidation, %</u>	
		<u>Local</u>	<u>Whole Core</u>
2.9	1816	3.4	0.25
4.6	1963	5.2	0.41 <sup>2</sup>
6.3	1873	4.8	0.40
8.0	1930	4.7	0.32
9.7 <sup>3</sup>	1823	3.7	0.29

<sup>1</sup> See the response to question number 30 on BAW-10174 and the response to question 5 on BAW-10166, Revision 2.

<sup>2</sup> See the response to question number 13 on BAW-10174 and the response to question number 2 on BAW-10168, Revision 1.

<sup>3</sup> See Appendix B (reanalysis of the 9.7' case at a higher  $F_c$ ).

### 3.0 Plant Description

The McGuire and Catawba nuclear power plants use nuclear steam supply systems (NSS) designed by Westinghouse that are representative of the standard Westinghouse four-loop, 3411 MWT design except for certain internal structures provided for the upper head injection system. The ECCS provided for the plant consists of the conventional combination of high pressure pumped injection, pressurized water storage tanks, and low pressure pumped injection all connected into the reactor coolant piping just upstream of the reactor vessel. Both the McGuire and Catawba plants use ice condenser containment systems.

#### 3.1 Physical Description

The reactor coolant system is enclosed entirely within a containment and is arranged into four heat transport loops, each of which has one recirculating steam generator and one reactor coolant pump. The reactor coolant is directed through the nuclear core within the reactor vessel, transported to the steam generators via four pipes (hot legs), cooled within the steam generator tubes, and returned to the reactor vessel through four cold leg pipes. Flow through the system is driven by four reactor coolant pumps, one per coolant loop. System pressure is maintained by a pressurizer connected to one of the coolant loops in the hot leg.

#### Reactor Vessel

The reactor vessel is basically a cylindrical shell with a hemispherical bottom head and a removable hemispherical upper head. Major regions of the reactor vessel are the inlet and outlet nozzles, the downcomer, the lower plenum, the core, the upper plenum, and the upper head. Coolant enters the vessel through one of four inlet nozzles and passes downward through the downcomer to the lower plenum. From the lower plenum, coolant is directed

upward passing either through the core or the baffle bypass region to the upper plenum. Within the upper plenum, the coolant mixes with a small amount of flow which was bypassed directly from the downcomer to the upper head and exits the reactor vessel through the hot leg nozzles.

In the original design, the McGuire units baffle bypass region was not connected in parallel with the core, but instead, connected to the top and bottom of the downcomer. During the 1980s, this design was shown to lead to damage of the core perimeter fuel assemblies by small, horizontal jets of fluid passing around the edges of the plates which form the baffle region. Altering the baffle region to connect between the inlet plenum and the outlet plenum substantially reduces the pressure drop between the core and the baffle region, and eliminates the fluid jets. In 1987, Duke Power contracted for the alteration of the McGuire units so that the baffle bypass region would be connected between the lower plenum and the outlet plenum. As this work is planned prior to the introduction of the Mark-BW fuel assembly into either of the McGuire units, this report considers both McGuire and Catawba plants to be of the upflow type within the baffle bypass region.

#### Reactor Core and Fuel Assembly

The reactor core is comprised of 193 fuel assemblies, with each fuel assembly consisting of 264 fuel rods, 24 guide thimbles, and one instrument tube. Each fuel rod consists of stacked fuel pellets contained in a Zr-4 fuel rod with a gap between the fuel pellet and the fuel rod. Fifty three of the fuel assemblies have control rod cluster assemblies used for power control and shutdown capability. McGuire unit I has silver-indium-cadmium control rods while the other units have B<sub>2</sub>C control rods. The Catawba and McGuire plants will be replacing the Westinghouse OFA fuel assembly with the Mark-BW fuel assembly. Both the Mark-BW and the OFA are 17 x 17 fuel rod arrays with an active length of approximately 12



feet. A comparison of fuel rod geometries for both fuel types is provided in Appendix A.

#### Reactor Coolant Loops

The coolant loop piping is connected to the reactor vessel through eight nozzles, all of which are located at the same elevation, approximately six feet above the top of the core. The outlet piping (hot legs) runs from the reactor vessel in a horizontal plane and undergoes an upward bend as it attaches to the steam generator inlet plenum. The steam generators are of the recirculating or U-tube type with vertical tubes and inlet and outlet plenums at a common elevation. The steam generator outlet pipe is bent to vertically downward at the plenum and continues downward for about 10 feet. At this point, the piping is bent through a 180 degree turn to vertical upward and rises to meet the reactor coolant pump casing. Discharge from the reactor coolant pump is horizontal and at the same elevation as the reactor vessel inlet nozzles, making the run of piping from the reactor coolant pump to the reactor vessel horizontal.

#### Steam Generators

The Catawba and McGuire steam generators are of the recirculating or U-tube design. Catawba Unit 1 and McGuire Unit 2 have Westinghouse model D3 preheat recirculating steam generators, McGuire Unit 1 has model D2 preheat steam generators, and Catawba Unit 2 operates with model D5 preheat steam generators. The operation of the steam generators for all four units is essentially the same. Most of the feedwater enters the steam generator on the cold side of the U-tubes, then travels up the tube nest to mix with fluid from the hot side of the U-tubes. The two-phase mixture from the tube nest then enters the separators wherein the steam is allowed to proceed to the steam generator upper dome, and the liquid is recirculated to the downcomer and back to the tube nest.

The generators differ only in the way they achieve feedwater preheating and in the positioning of the primary separators.

## 1.2 Description of Emergency Core Cooling System

The ECCS provided for the plants consists of the conventional combination of high pressure pumped injection, pressurized water storage tanks, and low pressure pumped injection all connected into the reactor coolant piping just upstream of the reactor vessel. Originally, the ECCS also included a direct upper head injection system. In 1987 the upper head injection system was shown to be unnecessary for accident mitigation and an encumbrance to plant maintenance that adversely affected plant operability. It has, therefore, been removed from the active plant systems and is no longer considered in plant licensing.

The high pressure injection capability of the plants is achieved through two systems: the Centrifugal Charging System (CC) and the Safety Injection System (SI). The Centrifugal Charging System is the highest pressure system of the ECCS, capable of injecting above normal operating system pressure, and is part of the makeup and purification system during normal operation. The system embodies sufficient redundancy such that one full train remains operative under the assumption of a single active failure. Emergency operation is activated automatically after receiving a Safety Injection Systems "S" signal, indicating low reactor coolant system pressure or high containment pressure. The Safety Injection System operates in the middle pressure range, capable of injecting up to about 1300 psia. It has two separate pumping sources with sufficient redundancy in the number of components to provide the required flow rate assuming a single active failure. The system is also actuated by the Safety Injection Systems "S" signal.

The accumulator system consists of four tanks, each containing about a thousand cubic feet of borated water and four hundred cubic

feet of nitrogen pressurized to 600 psi. The tanks are connected to the reactor coolant system at the reactor coolant pump discharge via pipes. Reverse flow during normal operation is prevented by in-line check valves. The system is, therefore, self-contained, self-actuating and passive. Flow into the RCS occurs whenever the reactor coolant system pressure falls below the tank pressure.

Low pressure injection is achieved with the Residual Heat Removal System (RHR). Normally used for cooling when the reactor is not operating, the system also serves the low pressure ECC injection function by providing borated water through the four accumulator injection nozzles. In emergency operation, the RHR pumps initially inject water from the Refueling Water Storage Tank (RWST). When the RWST is exhausted, the RHR pumps are aligned to take suction from the containment sump. During recirculation, the injection flow is passed through a heat exchanger before being returned to the reactor coolant system. The system contains sufficient redundancy such that one full train is available under a single active failure. Actuation is by the Safety Injection Systems "S" signal on low reactor coolant system pressure or high containment pressure. In its recirculation mode, the RHR injection system provides for long-term core cooling.

In the recirculation mode, only the RHR pump is capable of taking suction from the containment sump. However long-term, high pressure cooling is possible because both the Centrifugal Charging pumps and the Safety Injection pumps can take suction from the RHR pump discharge and deliver coolant through their cold leg connections to the RCS.

### 3.3 Plant Parameters

The major plant parameters and operating conditions are presented in Table 3-1.

Table 3-1 Plant Parameters and Operating Conditions

Reactor Power	3411 Mwt
Operating Pressure	2250 psia
Highest Allowable Total Peaking ( $F_c$ )	2.32
System Flow	$142.6 \times 10^6$ lbm/hr
Core Heat Transfer Area	59973 ft <sup>2</sup>
Average Linear Heat Rate	5.44 Kw/ft
Fuel Assembly	Mark-BW, 17 x 17 array
Fuel Pin OD	0.374"
Hot Leg Temperature	622 F
Cold Leg Temperature	562 F
Steam Generator Pressure	990 psia

#### 4. Analysis Inputs and Assumptions

The McGuire and Catawba plant evaluation performed for this report was conducted in accordance with the B&W Fuel Company recirculating steam generator LOCA evaluation model (Reference 1) using generic inputs which are conservatively applicable to both facilities. This chapter provides a brief discussion of the computer codes used in the evaluation, a discussion of plant parameters and assumptions, and an outline of the applicability of the generic inputs to the two facilities.

##### 4.1 Computer Codes and Methods

For the evaluation of cladding temperature transients and local oxidation, the B&W Fuel Company LOCA evaluation model consists of several computer codes. Figure 4-1 illustrates the interrelation of the computer codes used for the large break analyses. The RELAP5/MOD2-B&W code calculates system thermal-hydraulics and core power generation during blowdown. The thermal-hydraulic transient calculations are continued with the REFLOD3B code to determine refill time and core reflooding rates for the remainder of the transient. The FRAP-T6-B&W code is used to determine the hot pin temperature response during blowdown and refill. The BEACH code is used to determine the hot pin cladding temperature response during reflood with core flooding rates from the REFLOD3B outputs.

##### 4.2 Inputs and Assumptions

The major plant operating parameters used in the LOCA codes are:

1. Power Level - The plant is assumed to be operating in steady-state at 3479 Mwt (102% of 3411 Mwt).
2. Total System Flow - The initial RCS flow is  $142.6 \times 10^6$  lbm/hr.

3. Fuel Parameters - The initial fuel pin parameters are taken from TACO3 runs performed for the fuel assembly burnup which produces the highest peak cladding temperature. Sensitivity studies discussed in Chapter 6 show that fuel conditions at the beginning of life are the most severe for large break LOCA.
4. ECCS - The ECCS flows are based on the worst case between the assumption of a single active failure and the assumption of no failure. Sensitivity studies discussed in Chapter 6 show that the condition of maximum ECCS is the most severe assumption.
5. Total Peaking Factor ( $F_0$ ) - The maximum total peaking factor assumed by this analysis is 2.32.
6. The moderator density reactivity coefficient is based on beginning-of-cycle conditions to minimize negative reactivity.
7. The cladding rupture model is based on NUREG-0630.

#### 4.2.1 RELAP5/MOD2-B&W Modeling

The RELAP5/MOD2-B&W computer code is used to analyze RCS thermal-hydraulic behavior during the blowdown phase of a LOCA. RELAP5/MOD2-B&W, a modified version of the RELAP5/MOD2 code, is documented in BAW-10164 (Reference 2). RELAP5 permits the user to select model representation that results in a suitable finite difference model for the fluid system being analyzed. The nodalization for the plant evaluation, shown in Figure 4-2, was developed in accordance with the BWFC LOCA evaluation model (Reference 1).



The control volume inputs generally consist of volume geometry (area and height), flow-related parameters (resistance, hydraulic diameter, surface roughness), primary metal heat data, and initial conditions (pressure, temperature and flow). The non-equilibrium, non-homogenous option is used throughout, except for the core region, where the equilibrium, homogeneous option is selected in order to generate blowdown thermal-hydraulic data consistent with the formulation of the PRAP-T6 code. Flow paths are defined between control volume geometric centers. The B&W-developed SAVER computer code (Reference 3) is used to determine the initial pressure drops and flow distribution. RELAP5/MOD2-B&W is run in steady-state to assure proper initialization.

#### Core

As shown on Figure 4-2b, the reactor core model consists of an average channel (nodes 316-326), a hot channel (nodes 326-338), a core bypass region (nodes 340-344), and a baffle gap region (nodes 346-350). The hot channel consists of one assembly and the remaining 192 assemblies are modeled in the average channel. Axially, the powered regions are represented by six equal-length segments and the unpowered regions by three equal-length segments. Crossflow is allowed between the average core and the hot assembly using crossflow junctions. The resistance for these junctions is developed from the experimental correlation given in Section 2.2.7 of BAW-10092 (Reference 4). For the sensitivity studies and the break spectrum, the power distribution in the core is based on a symmetric chopped cosine with an axial peak of 1.5. For the LOCA limits studies, where the position of the axial peak varies with the case, the power shapes and peaking correspond to the case (refer to Chapter 8). Initial fuel temperatures, pressure, gas composition, and dimensions are based on beginning-of-life TACO3 calculations.

Reactor power during a LOCA transient is calculated using the RELAP5/MOD2-B&W kinetics option. The rate of heat generation within the fuel is computed by the code and is the sum of the fission power and fission product decay power. The point kinetics model used in the code accounts for changes in reactivity due to the effects of fuel temperature and coolant density. The action of control or safety rods is not credited by the BWFC LOCA evaluation model until after the end of blowdown. The Doppler coefficient is developed from end-of-life reactor physics calculations, so that fuel temperature decreases during LOCA maximize power generation.

### Reactor Vessel

The reactor vessel model consists of a downcomer (nodes 300-308), the lower head (node 310), the core inlet plenum (nodes 312 and 314), the core outlet plenum (nodes 352 and 354), the flow and support columns (node 356), and the upper head (nodes 358 to 364). In the downcomer, node 300 is centered at the core inlet nozzles. In the upper plenum, nodes 352 and 354 are centered at the vessel outlet nozzles. Reactor coolant bypass between the downcomer and upper head is taken into account by connecting node 300 to node 360. The upper plenum model differs from that of the non-upper head injection plant model in BAW-10168 due to the upper head injection (UHI) internals structure. The UHI flow columns and RCCA guide tubes are modeled with a separate volume (node 356), and the upper plenum is divided into two volumes (nodes 352 and 354).

### Reactor Coolant Loops

The loop noding scheme is a result of the loop noding and break noding sensitivity studies performed in Appendix A of BAW-10168. The four RCS loops are modeled as two by combining the three loops which will not contain the break simulation together. The 100 series nodes model the unbroken loops, and the 200 series models the broken loop. Within each loop, the hot legs are separated into

4 nodes; the RSG inlet plenum (nodes 120 and 220) and RSG outlet plenum (nodes 130 and 230) are single nodes. The RSG tubes (nodes 125 and 225) are separated into sixteen segments. The tube flow area is based on the assumption that 10 percent of the tubes have been plugged on the primary side and removed from service. The cold leg reactor coolant pump suction consists of 5 nodes, and the reactor coolant pump (nodes 160 and 260) is a single node. The connection from the reactor coolant pump to the reactor vessel is modeled as four nodes for the broken loop and as two volumes for the intact loop.

#### Reactor Coolant Pumps

The reactor coolant pump performance is developed from homologous relationships adjusted for two-phase degradation based on the data in Table 2.1.5-2 of BAW-10164. This is the same degradation data in NUREG/CR-4312 (Reference 5). In accordance with the LOCA evaluation model, the reactor coolant pumps are assumed to trip at the time of a LOCA.

#### Pressurizer

The pressurizer model consists of three parts: the surge line (node 400), an eight-section pressurizer (node 410), and a valve model (junction 415 and node 420). The initial condition for the pressurizer is saturated steam over saturated liquid with a void fraction specified in the interface node. The initial inventory for the pressurizer is set to approximate the normal operating level.

#### Recirculating Steam Generator

In agreement with the loop noding arrangement, the three steam generators associated with the unbroken loops are modeled as a combined region (700 node series) with the broken loop containing a

single generator (600 node series). The preheat region is modeled as the two lower volumes of the cold side tube riser (nodes 633 and 733). The five-volume cold side tube riser section (nodes 633 and 733) and four-volume hot side tube riser section (nodes 630 and 730) span the height of the generator tubes. Two volumes (nodes 635, 640 and 735, 740) are modeled below the separators. Nodes 650 and 750 are separator volumes. The steam dome is modeled by two nodes (660, 670 and 760, 770). The separator component in RELAP5/MOD2 acts as a steam separator and dryer with two-phase fluid entering from the bottom, steam exiting upward, and saturated fluid going back to the downcomer through nodes 655 and 755. Nodes 625 through 665 and 725 through 765 form the steam generator downcomer. Auxiliary feedwater as well as 13 percent of main feedwater is supplied at nodes 645 and 745. Nodes 675 to 680 and 775 to 780 provide simulation of the steam lines and the safety valves.

The heat structures which model the steam generator tubes are reduced in heat transfer area by about 5000 square feet to simulate 10 percent tube plugging. Heat structures of characteristic volume and surface area are also included for the shell, the downcomer walls, and separator components.

#### Break Characteristics

The four-node break location configuration is a result of the break noding sensitivity study in Appendix A of BAW-10168. Referring to Figure 4-2a, a double-ended guillotine break is modeled with leak paths from both nodes 270 and 275 to the containment. For a split-type break, the leak is modeled as a single path that leaves node 275. For the double-ended break, no flow is permitted between the leak nodes following the break. The switching criterion from subcooled (Extended Henry-Fauske) to two phase (Moody) discharge models is based on a leak node quality of 0.1 percent.

### Primary Metal Heat Model

All major components within the reactor vessel, loops and steam generators are considered. Within a specific region, primary metals are grouped together based on similar thickness and geometry. The exterior surfaces of the primary and secondary systems' pressure boundaries are assumed adiabatic to maximize stored energy in metal slabs.

### ECCS

The accumulators, centrifugal charging, safety injection, and residual heat removal systems are modeled by nodes 900 through 915. Injection is allowed only into the unbroken loops at the cold leg. The injection which would take place in the broken loop is assumed to flow directly into the containment. The accumulator is a passive system, check valve controlled, and activates automatically when the primary pressure falls below the tank pressure. The pumped systems are activated by the Safety Injection Systems signal of the Engineered Safety Features Activation System. Appropriate time delays for signal generation, electrical supply startup, and injection pump startup are accounted for in the initiation of the pumped injection systems.

### 4.2.2 REFL0D3B Modeling

The REFL0D3B code simulates the thermal-hydraulic behavior of the primary system during the core refill and reflood phases of the LOCA. The noding, shown in Figure 4-3, is consistent with the LOCA evaluation model and consists of reactor vessel and loop models. The reactor vessel is represented by a four fixed-nodes model; nodes 1R and 2R are volumes above and below the steam-water interface in the inner vessel region, and nodes 4R and 3R represent steam and liquid volumes, respectively, in the downcomer region including the lower plenum. The primary system piping is

represented by two loops similar to the RELAP5 blowdown model with a reduced number of volumes. Values for volume geometry and flow path hydraulic parameters are developed from the RELAP5 model.

RELAP5 results at the end of blowdown (EOB) define the starting point for the REFLOD3B calculations. The core and system initial conditions for REFLOD3B are derived from those of the associated RELAP5 run at EOB with appropriate accounting given for the differing noding details. In defining initial liquid inventory for REFLOD3B, the liquid remaining in the reactor vessel at EOB in RELAP5 is placed in the lower plenum of REFLOD3B. The initial flow rates, gas volumes, liquid inventories, and pressures of the accumulator tanks are taken directly from RELAP5. The reactor vessel (RV) steam volumes (1R and 4R) are initialized with saturated steam corresponding to containment pressure, and the loops contain superheated steam corresponding to the containment pressure and fluid temperature of the secondary sides.

The primary metal heat structures are also taken from the RELAP5 model with the initial conditions in REFLOD3B matching those in RELAP5 at the end of blowdown. The secondary side metal structures include a representation of the shell material. In mostly stagnate regions, such as the downcomer or lower head, the heat transfer coefficient is based on pool boiling or natural convection to vapor. In regions with flow, such as the hot legs or the steam generator, the heat transfer coefficient is set to 1000 Btu/hr-F for both vapor and liquid. These selections insure that the fluid leaving the steam generator is continuously dry steam superheated to the secondary side temperature.

For a double-ended pump discharge break, two leak paths are modeled, one from the RV upper downcomer (node 4R) and the other from the pump side of the break (node 28). The pump rotor resistance is based on the locked rotor condition. A 0.85 psi pressure drop is imposed on cold leg pipe junctions to account for



momentum losses due to steam-ECC water interaction during the accumulator injection phase. This value is reduced to 0.50 psi for the pumped injection once the accumulators have fully discharged.

The containment backpressures as a function of time from the Catawba FSAR are used in the REFLOD3B calculations. A comparison of the calculated mass and energy releases with those in the Catawba FSAR shows no significant differences between the BWFC and Westinghouse calculations. Therefore, the containment pressures previously calculated by Westinghouse are acceptable for the REFLOD3B analysis.

#### 4.2.3 FRAP-T6-B&W Modeling

The FRAP-T6 code is used to predict thermal responses of the hot fuel rod for the blowdown and refill phases of the LOCA. It is a boundary data driven code with inputs taken from RELAP5. The fuel rod axial and radial nodalization is identical to that of the BEACH model. Prior to transient calculations, the fuel rod is initialized, and the fuel temperature, fuel rod geometry and pin pressure are compared with the data predicted by TACO3. The boundary data inputs from RELAP5 are core power, hot channel flows, hot channel enthalpies, and system pressure. FRAP-T6 is terminated at the end of refill when water begins to enter the bottom of the core for reflooding.

#### 4.2.4 BEACH Modeling

The BEACH code is used to determine the hot fuel rod cladding temperature response during the reflooding phase of the LOCA. The BEACH model consists of a hot fuel rod and a flow channel with time-dependent inlet and outlet volumes to permit inputs of boundary data from the REFLOD3B calculations. The fuel rod is axially divided into 20 segments, as shown in Figure 4-4, with variable nodal length such that each grid is located at the bottom

of a node and three nodes are used to cover a grid span. The nodalization is basically that used in the code benchmarks in Appendix C of the BEACH topical report, BAW-10166 (Reference 6). Radially, the fuel pellet is divided into 7 equally spaced mesh points and two equally spaced mesh points for cladding.

The initial temperature distribution in the fuel rod and fuel pellet-clad gap conductance are obtained from the FRAP-T6 calculations at the beginning of reflooding. The boundary data inputs from the REFLOD3B calculations are inlet and outlet pressures, flooding rate, inlet water temperature, and core decay heat. The initial temperature of the steam surrounding the fuel rod is set equal to the cladding surface temperature.

If rupture occurs, BEACH is run in two passes. First, the code is run to the time of rupture. At this point, the cladding surface area at the location of rupture is increased, the blockage model applied, and BEACH restarted to the end of the analysis.

#### 4.3 Comparison of Plant Model with McGuire & Catawba

The plant model upon which the evaluations herein are based is neither McGuire nor Catawba specific, but rather, a generic model incorporating the features of both plants in a way which assures a conservative LOCA evaluation for either plant. For the most part, the McGuire and Catawba plants are identical at the level at which a LOCA evaluation can sense a meaningful difference in results. Table 4-1 compares selected data from the McGuire and Catawba FSARs to the values used in the generic model. The reactor coolant system volume, as reported in the FSAR, differs between the two plants by 4 percent. The model uses a value slightly lower than that reported for either plant. The loop flow areas are identical and the flow area through the steam generator tubes differs by only 2 percent. Therefore, the pressure distribution around the system is essentially identical. Furthermore, the positioning of the

major components relative to each other is the same. The Reactor Trip System and the Engineered Safety Features Actuation System of the two plants function the same. Therefore, if there are differences in the plants which can affect the results of a LOCA, they will lie in the attached systems.

The secondary or auxiliary system which can affect the results of a LOCA evaluation are:

- The Containment System
- The Secondary Coolant System
- The High Pressure ECCS pumped systems
- The Accumulator System
- The Low Pressure ECCS pumped system

The containment system can affect a LOCA result by imposing a different back pressure (containment pressure) for the reflooding calculations. With higher containment pressure during reflood, reflooding rates are higher and reactor core cooling is enhanced. The containment system for both facilities is of the ice condenser type of the same design and sizing. Thus, for the same accident, the containment pressure will be the same for the two plants. Furthermore, the containment pressure used in the LOCA calculation is already so low, because of the ice condenser design, that it would take a major deviation between the plants to cause a change in containment response significant to a LOCA evaluation.

The secondary system can affect a LOCA calculation through a change in the safety valve set point. During reflood, liquid carry-over from the core is vaporized in the steam generators and superheated to the saturation temperature of the secondary side. The evaluations set the secondary conditions according to the safety valves. If the temperature of the secondary coolant is reduced by a change in safety valve setting, the loop flow in KEFLOD3B will be less superheated and its specific volume, lower. This flow will,

therefore, be easier to move around the loop to the break, resulting in a lower loop pressure drop, higher core flooding rates, and colder cladding temperatures. The only substantial difference between the steam generators of these four plants is in the manner of achieving feedwater preheating, a difference that will not influence a LOCA result. The safety valve set point, for the two plants are the same.

The high pressure pumped injection is not of substantial import for a large break LOCA since the accumulators and low pressure systems provide more injection than can be effectively used in cooling the plant. Notwithstanding, the high pressure pumped injection flows used in the analysis were selected as the minimum of the flows of all four units for the minimum ECCS cases, and the maximum of the flows of all four units for the maximum ECCS cases.

The plants do differ in the accumulator system provided. The McGuire accumulator liquid volume is lower than Catawba's, and the pressurizing gas volume is higher. Also the surge line resistance for McGuire is higher than for Catawba. As the downcomer is full or nearly so before the accumulators empty, the different liquid volumes are unlikely to affect the LOCA results. The higher gas volume and higher line resistance for McGuire would tend to off-set each other, making it difficult to determine which configuration would produce the most conservative LOCA result. Therefore, a sensitivity study, presented in Chapter 7, was performed. Both configurations were evaluated; the Catawba configuration produced slightly higher cladding temperatures than the McGuire configuration. The accumulator used in the evaluation was based on the Catawba plant.

The low pressure pumped injection systems for both plants supply water at rates above those that can be effectively utilized in core cooling during LOCA. This is directly observed in reflood where, under a single failure assumption, the downcomer runs full

continually with about 20 percent of the ECCS injection spilling from the downcomer through the break. That water would be available to enter the core if higher flooding rates could be achieved, but the flooding is controlled by other plant features. Nonetheless, the low pressure pumped injection flows used in the analysis were selected as the minimum of the flows of all four units for the minimum ECCS cases, and the maximum of the flows of all four units for the maximum ECCS cases.

As has been shown, the McGuire and Catawba plants are essentially identical to one another at the level of design parameters that can affect the results of a LOCA. The single deviation which does exist between the plants has been the subject of a sensitivity study (Chapter 7) and shown to affect the cladding temperatures only slightly. Nevertheless, the most conservative of the two accumulator configurations has been used in these evaluations. Therefore, the studies and conclusions presented in this report are conservative for application to all four units of the Duke Power Company's McGuire and Catawba facilities.

TABLE 4-1 COMPARISON OF LOCA MODEL GEOMETRIC VALUES  
TO PLANT FSAR DATA

OVERALL SYSTEM

<u>Parameter</u>	<u>Plant Model</u>	<u>Catawba</u>	<u>McGuire</u>
Total System Volume Including Pressurizer, ft <sup>3</sup>	11,900 *	12,516	12,040
Total System Liquid Volume Including Pressurizer, ft <sup>3</sup>	11,100 *	11,774	11,298

REACTOR VESSEL

<u>Parameter</u>	<u>Plant Model</u>	<u>Catawba</u>	<u>McGuire</u>
RV I.D. at Flange, in	167.0	167.0	167.0
RV I.D. of Lower Shell, in	173.0	173.0	173.0
RV Inlet Nozzle I.D., in	27.5	27.5	27.5
RV Outlet Nozzle I.D., in	29.0	29.0	29.0
Overall Height of Vessel and Closure Head, ft	NA	43.8	43.8

REACTOR COOLANT LOOP COMPARISON

<u>Parameter</u>	<u>Plant Model</u>	<u>Catawba</u>	<u>McGuire</u>
Hot Leg Pipe I.D., in	29.0	29.0	29.0
Cold Leg Pump Suction Pipe I.D., in	31.0	31.0	31.0
Cold Leg Pump Discharge Pipe I.D., in	27.5	27.5	27.5
Pump Volume, ft <sup>3</sup>	78.6	78.6	57.0



TABLE 4-1 COMPARISON OF LOCA MODEL GEOMETRIC VALUES  
TO PLANT FSAR DATA (continued)

RECIRCULATING STEAM GENERATOR

<u>Parameter</u>	<u>Plant Model</u>	<u>Catawba</u>	<u>McGuire</u>
U-Tube Outer Diameter, in	0.75	0.75	0.75
Tube Wall Thickness, in	0.043	0.043	0.043
Number of U-Tubes	4,568 *	4,568	4,674
Heat Transfer Area, ft <sup>2</sup>	48,300 *	48,300	48,000

\* The values provided for the table are without tube plugging. The actual evaluation used reduced values to account for the assumed degree of tube plugging.

\*\* This value is not utilized in the plant modeling but has been supplied for the plants to illustrate the similarity between the McGuire and Catawba units.



- 4.17 -

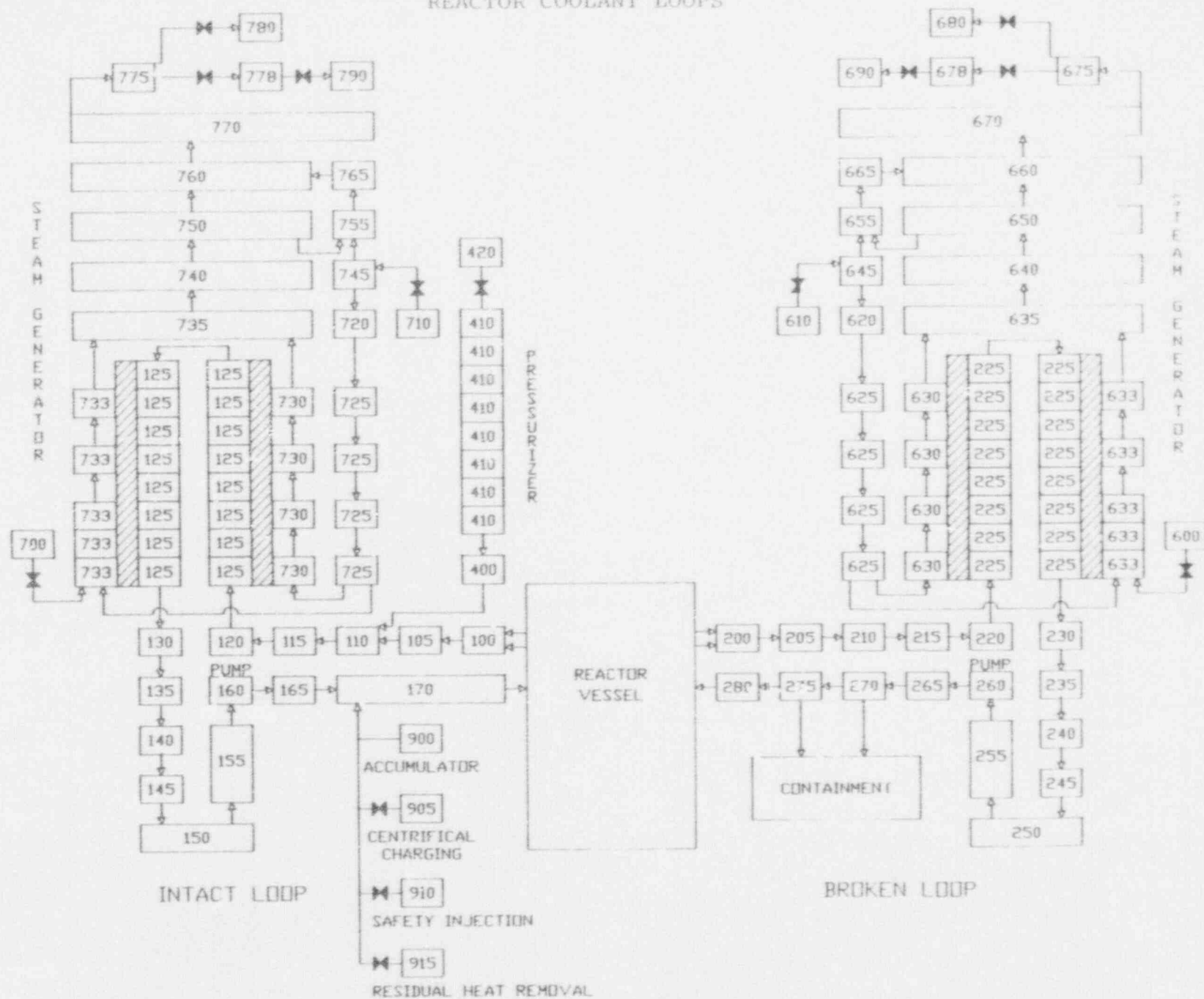


FIGURE 4-2b RELAP5/MOD2-B&W I.BLOCA NODING DIAGRAM

REACTOR CORE

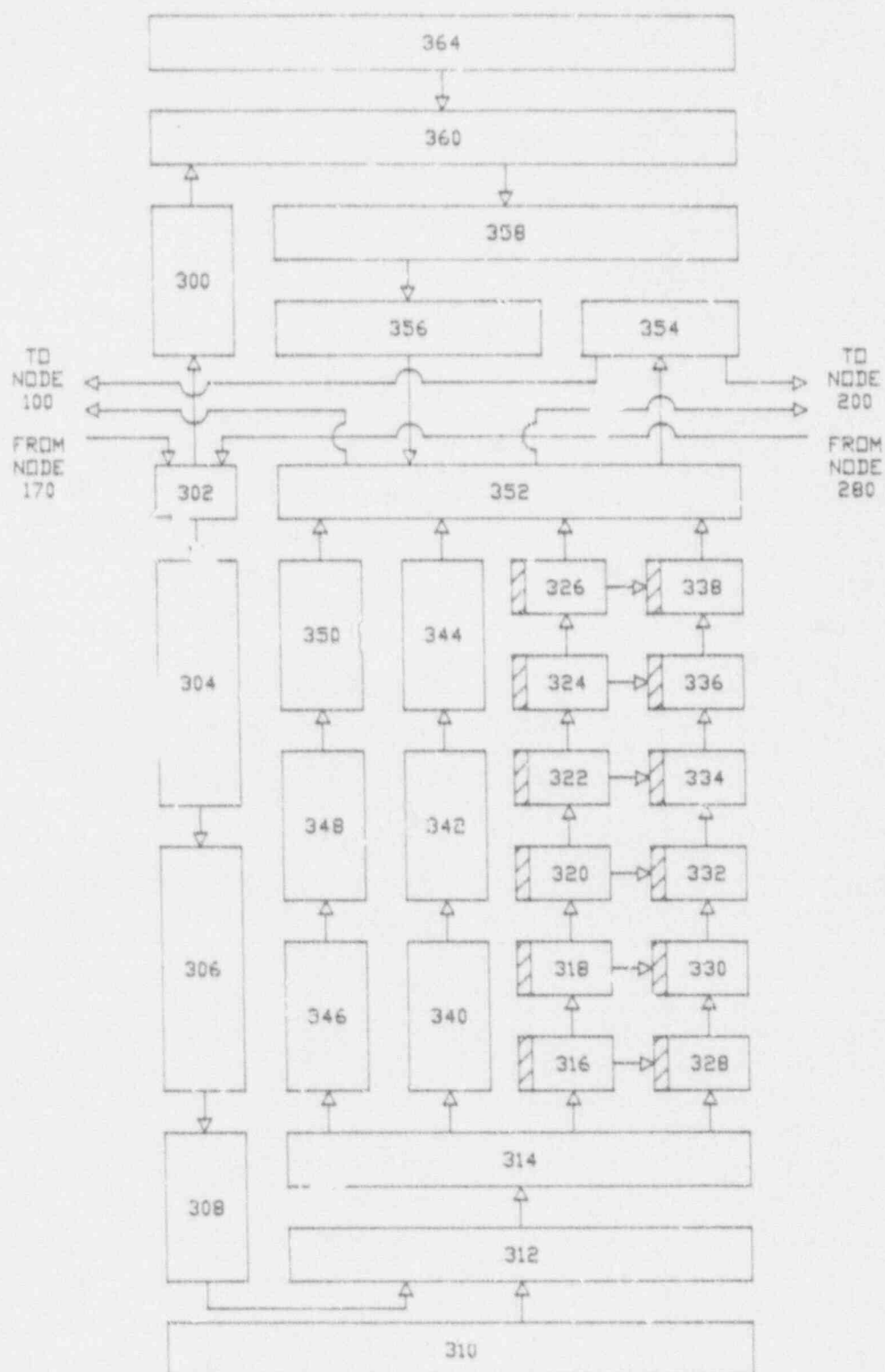


FIGURE 4-3 REFLOD3B NODING DIAGRAM

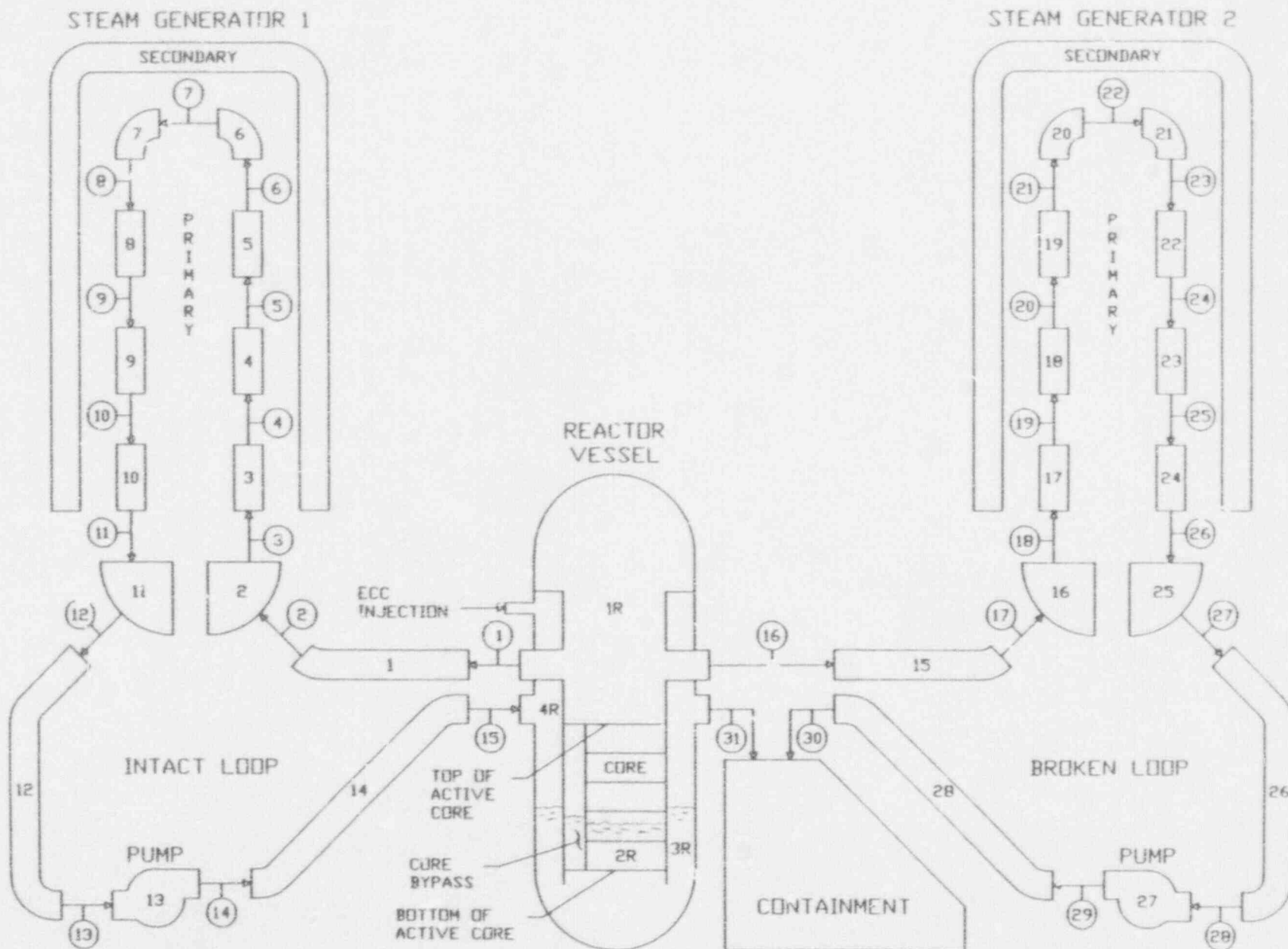


FIGURE 4-4 BEACH AND FRAP-T6-B&W NODING DIAGRAM  
FOR MARK-BW FUEL ASSEMBLY

		NODE ELEVATION
	20	12.00'
	19	11.28'
*	18	10.56'
	17	9.99'
	16	9.42'
	15	8.85'
	14	8.28'
	13	7.71'
*	12	7.14'
	11	6.57'
	10	6.00'
*	9	5.43'
	8	4.86'
	7	4.29'
*	6	3.72'
	5	3.15'
	4	2.58'
*	3	2.01'
	2	1.34'
	1	0.67'
*		0.00'

\* GRID NODE



## 5. Evaluation Model Changes

The original version of the BWFC large break LOCA evaluation model (Reference 1) was submitted to the NRC in July, 1988. Following that submittal, it became evident that the original evaluation model code package produced excessive conservatism in calculating certain aspects of the reflooding heat transfer processes. Consequently, improved methods were developed. The changes were incorporated in two steps: The first step involved changes to the BEACH code to refine the spacer grid effects model and to include rupture-blockage effects. The second step entailed modifying BEACH to include cladding metal-water reaction and active calculation of clad swelling and rupture using the same cladding behavior modeling as RELAP5/MOD2-B&W and FRAP-T6-B&W. This final modification thus allows BEACH to be used alone during the reflooding period to calculate peak cladding temperatures. These new models have been benchmarked against appropriate data and are being submitted to the NRC as revisions to the ECCS evaluation model topical report and to the affected code topical reports.

The analyses presented and discussed in this report were, of necessity, performed previous to or in parallel with the modeling revisions. The sensitivity studies described in Chapter 6.0 were done using the original ECCS evaluation model as reported in Reference 1. The plant-specific sensitivity and break spectrum analyses reported in Chapter 7.0 used the codes as applied in the original evaluation model but included the modifications to the BEACH spacer grid and rupture blockage models. Finally, the LOCA limits cases presented in Chapter 8.0 were executed with the final BEACH version, which included the clad swelling, rupture, and metal-water reaction models. The remainder of this chapter delineates the differences among the methods used and discusses the effects of these differences upon the calculated results.

Revision 0 of the BWFC LOCA evaluation model topical report (BAW-10168) was submitted to the NRC in July, 1988. Volume 1 of that report contained the large break LOCA model and Volume 2, the small break model. The model changes to be discussed in this section affect only the large break modeling. The code package and interrelationships for the original model are shown in Figure 5-1. RELAP5/MOD2-B&W is used to calculate system thermal-hydraulics and core power generation during blowdown. REFLOD3B determines the refill time and the core reflooding hydraulics for the remainder of the transient. The BEACH code is used to determine cladding surface heat transfer coefficients (HTC) with core reflooding hydraulics from REFLOD3B. FRAP-T6-B&W completes the analysis by using RELAP5 and BEACH inputs to evaluate the hot rod cladding temperature for the entire transient. This model is fully described and benchmarked in Revision 0 of the evaluation model topical report (Reference 1).

#### Initial Modifications to BEACH

The BEACH code version represented in the original evaluation model was found to contain two limitations. First, the fine drop vaporization model employed to predict the shattering of entrained liquid into small droplets at the spacer grids (and the controlling input parameter for that model) was inaccurate--and overly conservative--in terms of sensitivity to the grid blockage ratio. Second, the modeling of clad rupture effects was limited to those of increased cladding surface area at the rupture site. The additional effects, most notably those beneficial to interphase heat transfer and clad cooling above the rupture, were not represented.

The spacer grid effects model as originally formulated was found to be appropriate to account for the effects of simple spacer grids,

as had been used in the FLECHT, FLECHT-SEASET, and other experiments. Applying the model to grids with higher blockage ratios, particularly to the mixing vane grids used in the Mark-BW design, was found to be both crude and severely conservative. Simply put, the model was not capable of representing the mixing vanes other than as an increase in the thickness of the grid straps, that is, as a simple increase in the total blockage ratio. In addition, in developing the original model, it had been decided that the occurrence of rupture and the impact of the resultant channel blockage on interphase heat transfer would not be modeled. The first set of revisions to the evaluation model removed these limitations.

The grid fine-drop vaporization model was upgraded so that the droplet impact Weber number is calculated as a function of axial position within the grid, and a new correlation for the fine drop vaporization constant,  $c_{maxDB}$ , was developed. Second, the procedure for running BEACH was altered such that a blockage effects model, similar to a grid model, could be user-initiated at the location of rupture when rupture occurred. This required stopping BEACH near the time of rupture, executing FRAP-T6 with pre-rupture inputs from BEACH to determine the time and location of rupture, and restarting BEACH post-rupture with the model and appropriate inputs active. The first set of revisions preserved the code interrelationships shown in Figure 5-1.

#### Final Revisions

As work with the first revisions progressed, the results suggested that excessive conservatism remained in the model. It was found that the cladding temperatures calculated in FRAP-T6 were higher than those calculated in BEACH, and that the difference was greater than that attributable to the metal-water reaction calculated by FRAP-T6 and not in BEACH. The difference was primarily the result of the code interrelationships selected for the package (Figure 5-

1). FRAP-T6-B&W does not model axial conduction along the fuel pin. With 200 F to 300 F cladding temperature differences between ruptured and unruptured locations, the effect of axial conduction on the solution can be significant. The final model change was to replace the FRAP-T6-B&W code during reflooding with the BEACH code. This required the addition of metal-water reaction and clad strain and rupture models that were already available in RELAP5/MOD2-B&W (the parent code for BEACH).

In addition, the fine-mesh rezoning logic in the BEACH conduction solution was modified to use the maximum requested fine mesh near the quench front. A constant heat transfer coefficient of 528 Btu/hr-ft<sup>2</sup>-F below the quench front was adopted to stabilize vapor generation rates after quenching.

With these changes, the code applications and interface relationships became those of Figure 4-1. FRAP-T6-B&W is run only through the refill period, and BEACH, initialized to FRAP-T6, is started at the beginning of reflooding and run to the end of the analysis. If rupture occurs, BEACH is run in two passes. First, BEACH is run to the time of rupture. At this point the cladding surface area at the location of rupture is increased, the blockage model applied, and BEACH is restarted to the end of the analysis.

## 5.2 Effects of Evaluation Model Revisions

The changes to the evaluation model described in the foregoing section affect the calculated LOCA results only in the hot pin portion of the calculation and only during the reflood phase of the transient. The RELAP5/MOD2-B&W and REFLOD3B computer codes are completely unaffected by the changes, and the FRAP-T6-B&W analysis of the blowdown and refill phases is not impacted. For the most part, the original evaluation model remains intact, and the trends and conclusions derived from it continue to be applicable.

### Effects of Spacer Grid and Rupture Modifications

Because the grid model change was to make the model more responsive to the type of grid and thus, generate a more accurate solution, the effect of the modification depends on the type of grid being modeled. The original grid model was directly tied to spacer grids of conventional design (simple grids). Such grids have blockage factors (fraction of the flow channel area blocked by the grid) of about 30 percent. The revised model is formulated to be the same as the original when applied to simple grids. Thus, results produced by BEACH in benchmarks of experiments that used plain or simple spacer grids are unaffected by the model changes. For id designs with higher blockage ratios (such as zircaloy grids or mixing vane spacer grids), the blockage factors range from 45 to 60 percent and the blockage is distributed along the direction of flow. (For example, there may be a step to 40 percent blockage followed by a step to 60 percent blockage.) The effects of such grids on interphase heat transfer were found to be barely, if at all, distinguished in the original model from the effects of grids with the 30 percent blockage factor. It was obvious that the higher blockage grid should interact with more droplets and should cause a larger fine-drop effect than the lower blockage grid. The revised model accounts for this, and the interphase heat transfer, particularly due to fine-drop effects, is substantially increased for the higher blockage factor grids. Depending on the particular grid design, the amount of interphase heat transfer may increase considerably. Similarly, if a grid does not have a higher blockage factor, the amount of interphase heat transfer will be reduced by the new modeling.

The immediate effect of this change in the calculation is observed in the "grid nodes" of the hot channel simulation. At these locations, the vapor temperature can be reduced by several hundred degrees, and that can be directly reflected in the cladding temperature. However, this observation would apply to any



representative grid model, and the local effect is not the one of interest. Because the interphase heat transfer has been improved at the grid, the downstream locations within the span experience increased flow of the steam phase, and the temperature of the steam is reduced. Thus, heat transfer downstream of the spacer grid is improved. This interaction propagates downstream for several feet before it is essentially washed out. By the time (length) the effects of one grid have diminished, the flow encounters the next grid. The grid spans are typically 1.6 to 1.9 feet. Thus, the effect of this model change on the results in this report (the MARK-BW fuel assembly uses high blockage grids) is an overall decrease in cladding temperatures. This effect will pertain to the entire length of the fuel pin, but the timing may differ between the upper and lower elevations. Trends observed with the original model should be preserved in the revised model, but overall, the temperatures will be lower.

The effect of improving the interphase heat transfer at the location of rupture is nearly identical to that of the grid model. Here, the blockage factor is determined by the amount of swelling predicted by the rupture model. If that swelling is high, there will be a substantial increase in the interphase heat transfer. If the swelling is low, there will be very little change. For the cases considered here, the blockage factors from rupture are on the order of 50 to 60 percent, and the amount of resulting interphase heat transfer is significant. As with the grid model, the effect is on the interphase heat transfer and can directly impact the ruptured node cladding temperature. At other locations on the pin, the higher and colder steam flow from the ruptured location will provide greater cooling for nearly two feet downstream. Beyond that, there is no discernible effect.

Near the time of rupture, the node of highest power that does not contain a spacer grid will tend to have the highest temperature and thus become the ruptured node. With this model causing cooling in



the rupture node and in the next one or two nodes, the model creates the possibility that the rupture location and the location of peak cladding temperature will be separated by one grid span. Previously, the peak cladding temperature was predicted to occur in one of the nodes adjacent to the ruptured node. With the revised model, the node immediately above the ruptured node is provided with additional cooling to the extent that the node centered in the grid span above the location of peak power will produce a very close, if not higher, cladding temperature.

Taken together, the grid and rupture model changes produce a generally increased cooling of the entire fuel pin, a more dramatic temperature reduction in the grid nodes and the ruptured node, and allow a shift in the peak cladding temperature to a grid span removed from the location of peak power. These changes do not necessarily affect trends produced by the original model. Where the original trends pertained to behavior of the fuel pin globally, they will remain applicable. However, those trends that were local effects, particularly near the rupture location, may be altered.

#### Effects of the Final Changes

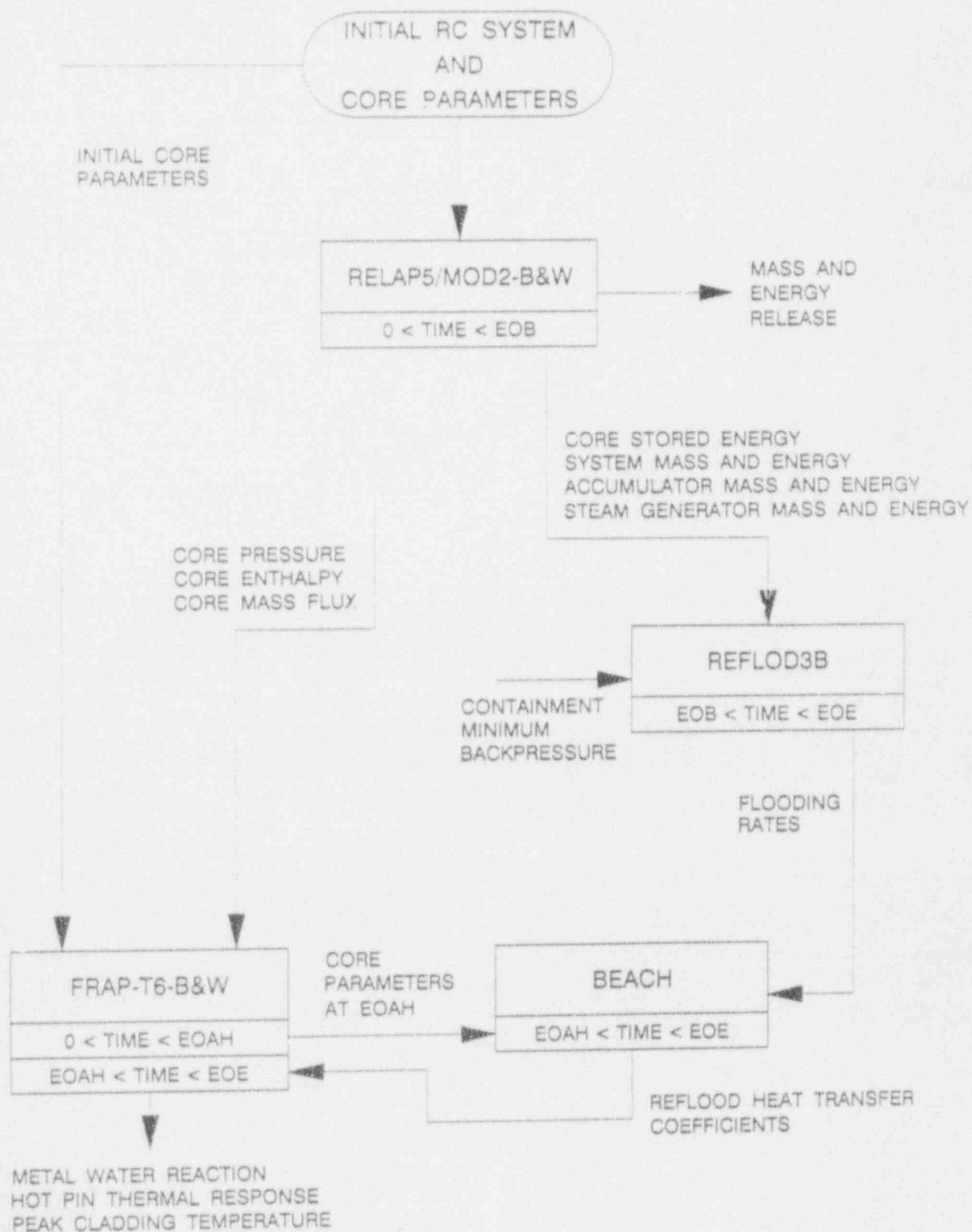
The effects of incorporating axial conduction into the hot channel calculation are substantial at the ruptured node and in the nodes adjacent to it. Without axial conduction, the ruptured node can be 200 F to 300 F below the adjacent nodes. With axial conduction, sufficient heat is transferred to the ruptured node that it runs only 100 F to 150 F below the adjacent nodes. Those nodes in turn are up to 100 F to 150 F cooler than calculated in the original model. The model also affects the grid nodes and adjacent nodes in the same way, though the effects are less marked. That is simply because the temperature gradients between the grid nodes and

neighboring nodes are not so severe. The net effect, though, is the same: axial conduction reduces the temperature differences between adjacent heat structures.

The other model changes in the final set were refinements to remove spurious oscillations, driven by switching in the logic. They are found to produce essentially no difference in peak cladding temperatures. Post-peak cooldown is smoothed, by the use of 32-point fine-mesh rezoning, for nodes at and above the midplane.

The cumulative effect of the model changes on the calculated LOCA results is an overall reduction in predicted cladding temperatures. Post-rupture temperatures at locations adjacent to the ruptured node are noticeably reduced. With the additional cooling in the rupture node, and in the adjacent nodes through axial conduction, the model has the effect of separating the rupture location and the location of peak cladding temperature by one grid span. The trends of sensitivity studies observed with the models still pertain if those trends were developed over the entire fuel pin. Trends derived from specific locations on the fuel pin may need to be reevaluated for impact and may require confirmation.

FIGURE 5-1 REVISION 0 EVALUATION MODEL  
CODE INTERFACES FOR LARGE BREAK LOCA



## 6.0 Sensitivity Studies

LOCA evaluations require that a substantial number of sensitivity studies be performed with the evaluation model in order to establish model convergence and conservatism. Most of the studies upon which the evaluations in this report are based are generic and were documented in the evaluation model report, BAW-10168 (Reference 1). Studies such as break spectrum and worst case ECCS configuration are considered plant-specific and are documented in this report. This chapter provides a discussion of the generic sensitivity studies from the reference evaluation model report that have been applied. The plant-specific sensitivity studies are presented in Chapter 7.

### 6.1 Evaluation Model Generic Studies

Of the sensitivity studies presented in the original evaluation model topical report (Reference 1), the majority are generic and would apply to any plant evaluated. Those studies considered generic each demonstrate results that are characteristic of the evaluation model--the codes and interfaces--and that are not plant dependent. An example of this is the RELAP5/MOD2-B&W time step study, which demonstrated that the automatic time step selection in RELAP5/MOD2-B&W would produce converged results. This demonstration need not be repeated for plant-specific applications wherein the modeling techniques used are represented by those in the evaluation model studies. The following is a listing of the sensitivity studies considered to be generic, with a discussion of why the conclusions of the study remain applicable for this applications report. In particular, the continued applicability is discussed relative to the later modifications to portions of the evaluation model. For convenience of review, each discussion is referred to the section in the evaluation model report where the study is documented.

#### RELAP5/MOD2-B&W Time Step Study

This study (BAW-10168, Appendix A, Section A.2.1) verified that for light water reactor geometry, the RELAP5 time step controller governs the code solution sufficiently to assure converged results. Alternate system designs within the group to be covered by the evaluation model will not change that result. The differences between the evaluation package used for the study and the evaluation model used for this report do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

#### RELAP5/MOD2-B&W Loop Noding Study

This study (BAW-10168, Appendix A, Section A.2.2) verified the general noding requirements within the loop for recirculating steam generator plants. In conjunction with the break noding study, the results can be applied to the separate regions of the hot leg, the steam generator, and the cold leg. Alternate system designs within the group to be covered by the evaluation model will not change the noding requirements. The differences between the evaluation package used for the study and the evaluation model used for this report do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

#### RELAP5/MOD2-B&W Break Noding Study

This study (BAW-10168, Appendix A, Section A.3.1) verified that hydraulic stability is achieved by providing at least one control volume in the pipe between any adjacent component and the break node. The break noding study is applicable to all plants covered by the evaluation model. The differences between the evaluation package used for the study and the evaluation model used for this report do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.



#### RELAP5/MOD2-B&W Pressurizer Location Study

Although the assumption placing the pressurizer in one of the intact loops was somewhat conservative, this study (BAW-10168, Appendix A, Section A.3.2) showed that there is little difference in results when the pressurizer is modeled in the broken loop. The lack of sensitivity to pressurizer location is expected to hold for all designs covered by the evaluation model and this study will not be repeated. The differences between the evaluation package used for the study and the evaluation model used for this report do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

#### RELAP5/MOD2-B&W Core Crossflow Study

This study (BAW-10168, Appendix A, Section A.3.4) verified that cross flow in a light water reactor is limited and does not alter the course of a LOCA evaluation substantially. The study is dependent only on the very basic aspects of the fuel design, which are consistent across the range of designs considered by the evaluation model. The differences between the evaluation package used for the study and the evaluation model used for this report do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

#### RELAP5/MOD2-B&W Core Noding Study

In conjunction with the core cross flow study, this study (BAW-10168, Appendix A, Section A.3.5) verified that the modeling of a light water reactor core in six axial segments with a hot and an average channel provides sufficient spacial detailing for both model convergence and result accuracy. As the basic fuel arrangement and fuel design are not altered across the range of designs to be considered, the results of the study are applicable



to all plants considered by the evaluation model. The differences between the evaluation package used for the study and the evaluation model used for this report do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

#### REFLOD3B Primary Coolant Pump Rotor Resistance Study

This study (BAW-10168, Appendix A, Section A.2.4) showed a considerable reduction in flooding under a locked rotor assumption. The study affirms the generally accepted data on loop resistance effects on reflooding rates and is applicable for all plant types covered by the evaluation model. The differences between the evaluation package used for the study and the evaluation model used for this report do not affect the REFLO.3B code. Therefore, the study remains applicable.

#### FRAP-T6-B&W Time Step Study

This study (BAW-10168, Appendix A, Section A.3.6) verified that the time step selection for FRAP-T6-B&W provided converged results for the spatial detail modeled in the base runs. Because the spatial detail required for the FRAP-T6-B&W model is not to be altered for the other designs covered by the evaluation model, the study remains valid for all designs. The differences between the evaluation package used for the study and the evaluation model used for this report only shorten the period of the LOCA for which the FRAP-T6 code is applied to the blowdown and refill phases. As the model changes do not directly affect the FRAP-T6 code nor the blowdown and refill periods, the study remains applicable.

#### FRAP-T6-B&W Radial Fuel Segmentation Study

This study (BAW-10168, Appendix A, Section A.3.7) verified that the number of solution points selected for radial representation of the

fuel pin used by the base FRAP-T6-B&W model was adequate. The study is dependent only on the very basic aspects of the fuel design, which are consistent across the range of designs considered by the evaluation model. The differences between the evaluation package used for the study and the evaluation model used for this report only shorten the period of the LOCA for which the FRAP-T6 code is applied to the blowdown and refill phases. As the model changes do not directly affect the FRAP-T6 code nor the blowdown and refill periods, the study remains applicable.

#### 6.2 Confirmable Sensitivity Studies

In addition to the generically applicable studies, some of the studies performed for the evaluation model are considered confirmable. These studies remain valid under most but not all circumstances. The following is a listing of such sensitivity studies with a discussion of why the conclusions of the study can be applied to the McGuire and Catawba evaluations, why they remain valid in light of the alterations in the evaluation model, and a reference to the section in the evaluation model report where the study is documented.

##### RELAP5/MOD2-B&W Pump Degradation Study

This study (BAW-10168, Appendix A, Section A.3.3) established a most severe pump degradation multiplier by altering the pump effects on the core flow. The study can be applied to all plants which experience similar LOCA core flow histories during blowdown. The core flow shown for the McGuire/Catawba evaluation base case (a guillotine break at the pump discharge with a Cd of 1.0) in Figure 6-1 is of the same general form as the core flow for the reference case in the evaluation model report, also shown in Figure 6-1. Therefore, the applicable debilitating effect of the degradation has been preserved across the plant design and the results of the study are applicable to the McGuire and Catawba evaluation. The

differences between the evaluation package used for the study and the evaluation model used for this report do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

#### REFLOD3B Loop Noding Study

This study (BAW-10168, Appendix A, Section A.2.3) verified the noding detail used in the REFLOD3B code. It is applicable to plants with a one-to-one correspondence of hot and cold legs, such as the McGuire and Catawba units. A separate study is required only for severely altered loop designs, such as the B&W or Combustion Engineering 2-by-4 designs. The differences between the evaluation package used for the study and the evaluation model used for this report do not affect the REFLOD3B code. Therefore, the study remains applicable.

#### Time-in-Life Study

This study (BAW-10168, Appendix A, Section A.3.9) showed the initial fuel temperature to be the dominant fuel burnup-related parameter affecting the LOCA results. There are three fuel parameters that vary significantly with fuel burnup: initial fuel energy, internal pin pressure, and oxide thickness. The initial stored energy of the fuel, given by the volume-average temperature at any specific local power level, changes with burnup. The pattern observed is that the fuel temperature at a specific local power may increase sharply during the first few days of operation, but it will then decrease until relatively high burnups, after which a slow increase may occur. Calculations with TACO3, Figure 6-2, have shown that the initial rise in temperature takes place virtually at the start of fuel operation. From that point, there remains a long burnup period with decreasing fuel temperatures. The fuel pin internal pressure undergoes a continuous increase with

burnup. The third parameter, of somewhat secondary importance, is the oxide thickness, which increases with fuel burnup.

While the interactions among these parameters in a LOCA analysis are complex--depending somewhat on the complexity of the model being used--the sensitivities of the results to each can be generalized: The higher the average fuel temperature at any location on a fuel pin at the start of a LOCA, the higher the resultant cladding temperatures at that location during the LOCA. The higher the internal pin pressure at the initiation of the LOCA, the more likely or earlier the occurrence of rupture will be. This may or may not have any bearing on the peak cladding temperature. The thicker the initial oxide layer of the fuel pin, the lower the cladding temperatures for cladding that has temperatures above about 1800 F. The oxide effect results from metal-water reaction and is relatively inconsequential for cladding that is not heated to 1800 F.

The effect of rupture, and thus, pin pressure on cladding temperature is model-dependent. For the computer codes and methods with which the original evaluation model sensitivity studies were performed, a rupture after the end of blowdown had essentially no effect on the cladding temperature at any location other than at the rupture. At that location, the effect of rupture was to allow the gap and the cladding surface area to increase, both of which act to decrease the cladding temperature. Thus, the effect of rupture during reflooding was to reduce the temperature increase of the cladding at a location that, clad rupture notwithstanding, would not have been the peak temperature location. A rupture during blowdown was modeled to have one additional effect: The blowdown rupture would cause additional flow resistance to be calculated in the hot bundle. This would cause some flow diversion away from the hot assembly and result in poorer blowdown cooling for both the ruptured location and the rest of the hot pin. The rupture location would, nonetheless, remain relatively cooler than

other locations on the pin, but those other locations would have higher cladding temperatures because of the blowdown rupture.

These results and observations lead to the general conclusion that, for fuel pin temperatures and internal pressures that do not produce a blowdown rupture, the most restrictive burnup for the LOCA to be analyzed is the one with the highest initial fuel temperature. If blowdown ruptures are to be considered, a more detailed study is required to determine the most severe combination(s) of temperature, pin pressure, and oxide thickness. This is, in fact, what was observed in the sensitivity study performed for the evaluation model report (Reference 1). This conclusion is independent of the fuel and plant designs and thus applies to any fuel or plant of reasonably similar design that is determined not to encounter a blowdown rupture. These conditions, and thus the conclusion, apply to the McGuire and Catawba units as will be further described.

The foregoing discussion is derived from the codes and methods used in the sensitivity studies presented in the original evaluation model topical report. The recent evaluation model changes do not directly affect the occurrence of rupture during blowdown. The changes do, however, extend the calculated effects of rupture during reflood. In the latest model, all of the previously modeled aspects of rupture are included, with additional effects. The blockage of the flow channel by the expanded cladding will interact with the entrained liquid--droplets--in the dispersed flow causing smaller droplet shattering. This produces an increase in the calculated interphase heat transfer and thereby causes lower vapor temperatures and higher steam flows at the rupture location. Both of these propagate up the bundle channel to promote better cooling in the adjacent nodes. Additionally, because BEACH models axial conduction in the cladding, once the ruptured node expands to as much as twice the heat transfer area, the resulting cladding temperature gradients permit substantial transfer of heat from the

unruptured locations on either side of the rupture to the ruptured location. Both the increased interphase heat transfer and the axial conduction produce additional cooling in nodes beyond the immediate rupture location.

Considering the effects of cladding rupture in the latest model separately, it could be inferred that, because a predicted rupture improves the cooling and reduces the temperature increase during reflood for a portion of the cladding beyond the rupture site, earlier prediction of rupture would result in lower calculated peak cladding temperatures. Taking this to be the case, it further develops that the relationship between pin internal pressure--the hoop stress leading to rupture--and fuel average temperature is such that the pin pressure is lowest at the burnup conditions producing the highest fuel average temperatures, namely at BOL conditions. The coincidence of these conditions continues to support the conclusions related to burnup drawn from the studies of the original evaluation model: For fuel pin pressures and temperatures not producing rupture during blowdown, the limiting LOCA initial conditions are those at beginning-of-life.

The analyses presented in this report are done using TACO3-calculated fuel inputs for BOL conditions. To establish BOL as the worst case, the possibility of a blowdown rupture must be precluded. It is evident that a rupture during blowdown does not occur for the BOL case in the analyses presented in this report. Confirmation of the same for end-of-life (EOL) conditions can be accomplished by comparing TACO3 EOL fuel parameters to those used in the time-in-life study in the original evaluation model report.

The TACO3 volume-averaged fuel temperature at 12.6 kw/ft at 60,000 MWd/MTU is slightly less than 1800 F. The internal pin pressure is limited by technical specifications to the operating system pressure of 2280 psia. The corresponding conditions for the sensitivity study at 60,000 MWd/MTU were 1911 F and 2280 psia, and



the predicted rupture occurred well into the reflood phase. As the evaluation model has not been altered for the blowdown and refill phases, these results continue to apply, and the new TACO3 data will not produce a blowdown rupture for burnups as high as 60,000 MWd/MTU.

Figure 6-2 shows representative volume-averaged fuel temperatures as calculated by TACO3 at three different local power levels as a function of pellet burnup. The power level marked "high local power" is somewhat above that used at the peak power locations in these analyses. As shown, the volume-averaged fuel temperature decreases substantially for early burnups and continues to decrease to 30,000 MWd/Mt after which it undergoes a slight increase. By 60,000 MWd/MTU, the difference between the volume-averaged fuel temperature and the coolant temperature is still only 80 percent of that at the beginning of life. Therefore, the worst time in life to perform a LOCA evaluation is at the beginning of life. The time actually selected for the TACO3 inputs for this report was 1 MWd/MTU.

The studies for burnup in the referenced evaluation model report have been shown to support a basis for the selection of the worst time in life to evaluate a LOCA given that a blowdown rupture cannot occur. These same studies, when compared to TACO3 fuel temperatures, assure that a blowdown rupture will not occur for the McGuire and Catawba analyses. The evaluation model studies determine that the beginning of life is the most conservative time to assume for the LOCA analysis, and this conclusion applies for both the codes and methods of Reference 1 and the current evaluation model. Thus, the time in life for the McGuire and Catawba LOCA evaluations and calculations is established as beginning-of-life.

In general, break spectrum studies are plant-specific in terms of applicability. The break location studies, however, which have typically been included among the break spectrum cases can be considered generic. There are three break locations to consider for the large break LOCA: the hot leg piping, the cold leg piping at the pump suction, and the cold leg piping at the pump discharge-between the pump and the reactor vessel. Both the hot leg break and the pump suction break have been consistently shown to result in peak cladding temperatures below those predicted for pump discharge breaks. (This is demonstrated in the Westinghouse RESAR for the 3411 Mwt class and is consistent with the numerous analyses reported by B&W for B&W-designed PWRs.) The analysis in Appendix A of Reference 1 concluded that the blowdown and reflood cooling for the pump suction break were sufficient to dismiss the suction break as a potential limiting case strictly on the basis of the blowdown and reflood system analyses.

An examination of the consequences of the break location explains these observations. With the break at the pump discharge, one accumulator and approximately 25 percent of the available pumped injection will be bypassed directly to the containment, not providing blowdown cooling and lengthening adiabatic heatup. Flow through the core during blowdown for the hot leg and the pump suction breaks will be more positive than for the pump discharge break because of the leak position in the loops. For the hot leg break, there can be no bypass of the injected ECC water unless that water has already passed through the core. Furthermore, the elevation head driving reflood can rise to as high as the spillover elevation in the steam generator tubes.

For the pump suction break, the core flooding also improves because the break is moved to the other side of the pump. The effect,

however, is not as strong as for a hot leg break. Therefore, in conjunction with the better blowdown cooling, the suction break produces a lower peak cladding temperature than does the pump discharge break. While the lower peak temperature alone precludes the suction break from being limiting, there is further reason to concentrate analyses upon breaks in the pump discharge piping. Because of the volume of pumped ECCS flow spilled from the system for the pump discharge break (roughly 25 percent), that condition represents a more limiting ECCS flow than does the pump suction break. For these reasons, and based upon the discussions of the foregoing paragraphs, the spectrum and LOCA limits cases performed for Catawba and McGuire are for large breaks in the reactor coolant pump discharge piping.

FIGURE 6-1 CORE MASS FLUX  
EM SPECTRUM STUDY VERSUS McGUIRE/CATAWBA BASE MODEL

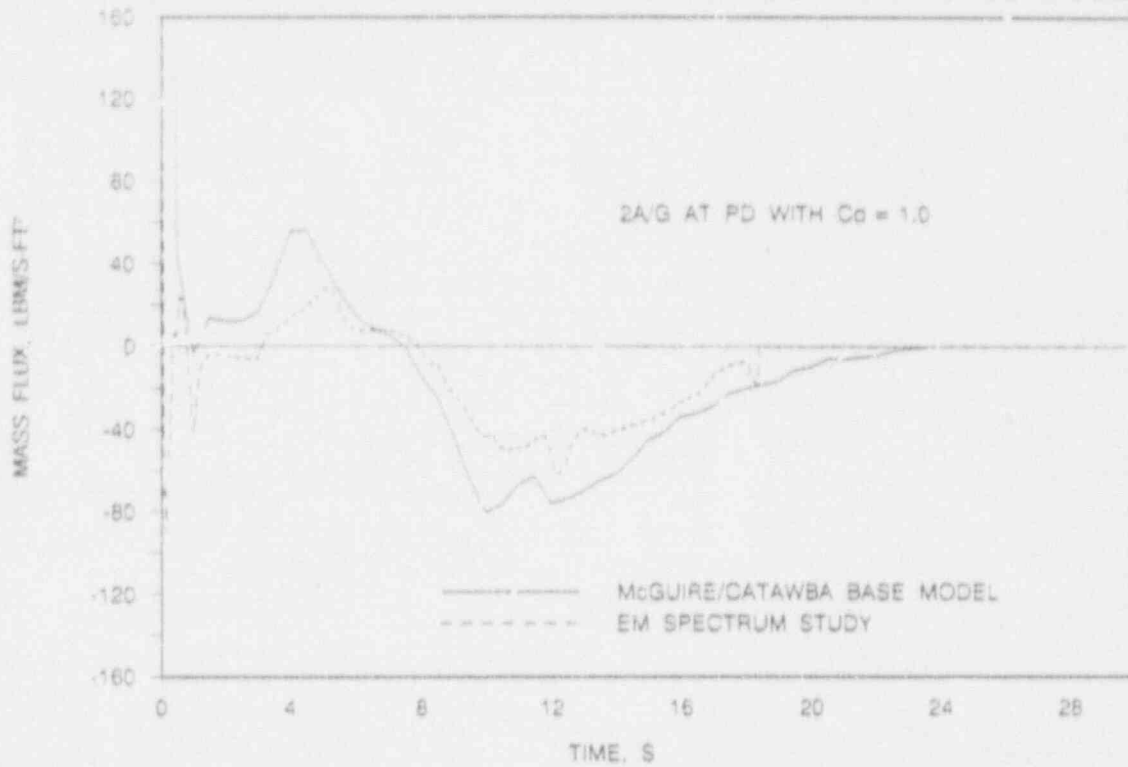
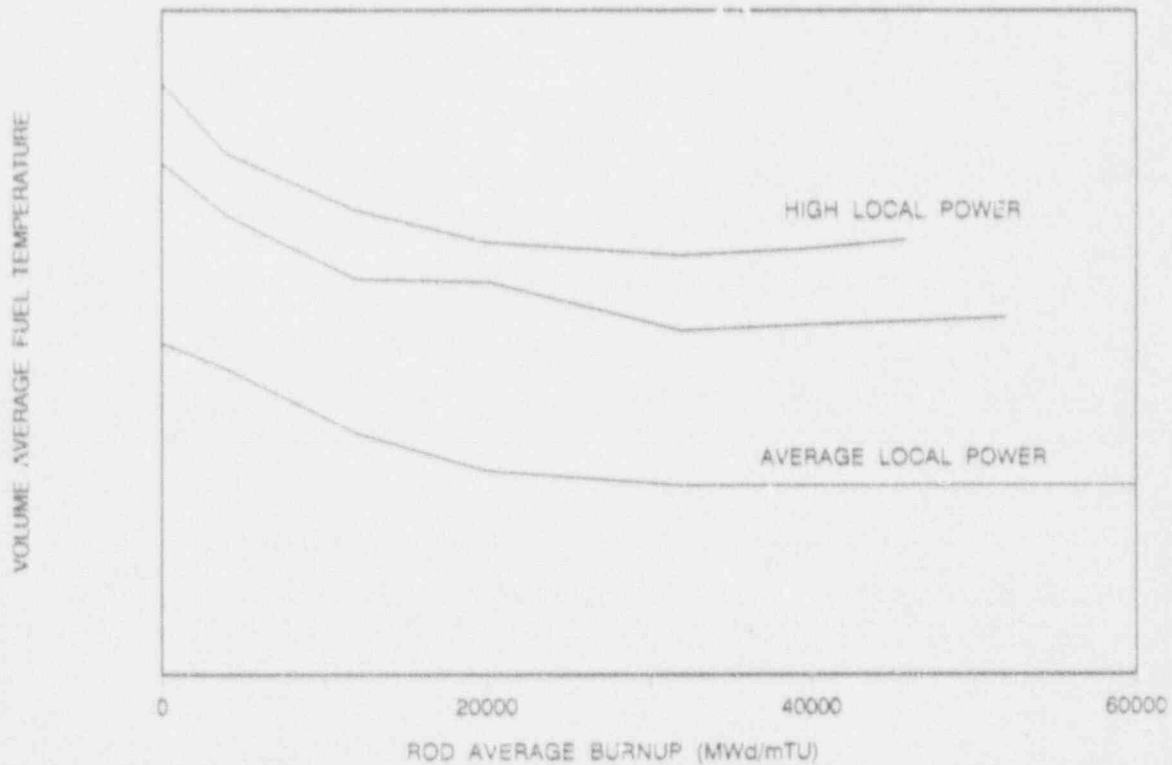


FIGURE 6-2 TACO3 FUEL TEMPERATURE AS A FUNCTION OF BURNUP



## 7. Plant-specific Studies and Spectrum Analysis

Although a considerable portion of the analysis inputs and assumptions can be set by the evaluation model and its sensitivity studies, some parameters are dependent on plant-specific inputs and can only be established by individual plant studies. These studies and the spectrum analyses are performed to identify a worst case break to use in calculating the LOCA Limits. Figure 7-1 shows the order in which the plant-specific sensitivity studies and the spectrum analysis cases were performed. The calculations were all done with the changes to the BEACH spacer grid and rupture models (the first step in the modifications) described in Chapter 5. This chapter presents the results of the studies and describes applicability of the results to analyses done with the final evaluation model, which incorporates all of the modifications.

### 7.1 Base Case

The first step in performing a series of sensitivity studies is to establish a base case. For the studies presented in this chapter, the base case is a double-ended guillotine cold leg break, with a discharge coefficient of 1.0, located between the reactor coolant pump and the reactor vessel. Figures 7-2 through 7-9 present key parameters for this case. The results compare well to previous cases analyzed with the original evaluation model, done for a standard 3411 Mwt plant with dry containment. The major differences in the results are in the length of blowdown, which is approximately four seconds longer, owing to the lower pressure of the ice condenser containment in this base case. Core flow is affected by the lower initial fluid temperatures in the upper head region, as shown by negative flows greater than those calculated for the design assumed in the evaluation model case. Core reflooding is slower because of the additional steam binding encountered for the lower containment backpressure. This lower

flooding rate is somewhat alleviated by the venting paths afforded by the upper head spray nozzles, also not a feature of the configuration assumed for the original EM studies.

## 7.2 Accumulator Configuration

As was discussed in Chapter 4, one difference between the McGuire and the Catawba plants is the design of the accumulators and the accumulator surge lines. The McGuire accumulator liquid volumes are lower than those of Catawba, and the pressurizing gas volumes are higher. Also, the surge line resistances for McGuire are higher than for Catawba. The table below contrasts parameters for the plants.

<u>Parameter</u>	<u>McGuire</u>	<u>Catawba</u>
Accumulator Liquid Volume, ft <sup>3</sup>	950	1050
Accumulator Gas Volume, ft <sup>3</sup>	450	350
Accumulator Tank Pressure, psia	615	615
Supply Line Flow Area, ft <sup>2</sup>	0.42	0.42
Supply Line Resistance Factor	12.4	5.7

Since the downcomer is full, or nearly so, before the accumulators empty, the different liquid volumes are unlikely to affect the LOCA results. The higher gas volume, which will maintain a higher tank pressure during discharge, and the higher line resistances for McGuire, would tend to be offsetting. Therefore, it is not surprising that the study showed essentially no difference in peak cladding temperature. Selected parameters for these runs are shown in Figures 7-10 and 7-11. The analysis included the BEACH grid and rupture blockage modifications but only altered input parameters for RELAP5/MOD2-B&W and REFLOD3B to form the comparison. Neither of these codes is affected by the evaluation model changes, and the results are consistent over the length of the pin (see Figure 7-11). Thus, the study remains valid for use with the final evaluation model.



The results do not show a significant difference in effects between the two accumulator configurations. The Catawba configuration, because it is 4 F higher in peak cladding temperature, is used for the remaining studies in this chapter and for the LOCA limits studies.

### 7.3 Break Spectrum Analysis

The break spectrum analysis is performed to determine the worst case break size, the worst case break configuration, and the worst case break location. The break location study was discussed in Section 6.3 with the conclusion that the hot leg and reactor coolant pump suction breaks are assured to be substantially less limiting than the pump discharge breaks because of the lack of ECCS spillage for these events. The differences between the split and the guillotine break and the range of break sizes cannot be generalized, however, and those studies were run for the McGuire/Catawba evaluation. The break size study was performed first and followed by the break type study.

For the BWFC large break evaluation model, the break size study is interchangeable with a discharge coefficient study since the break flow is directly proportional to the product of the break area and the discharge coefficient. For the McGuire/Catawba model, the discharge coefficient study was conducted for a guillotine break of twice the area of the cold leg piping located at the pump discharge with discharge coefficients of 1.0, 0.8, and 0.6. Key parameters for these cases are shown grouped by case in Figures 7-12 through 7-35. Table 7-1 presents a comparison of the timing of events for the three cases.

There are no major differences among the sequences of events for the three cases that make up the discharge coefficient study; the peak cladding temperatures differ by only 110 F. As expected,

blowdown is extended as the break flow is decreased. The 0.8 and 0.6 discharge coefficient cases remove slightly more heat from the fuel during blowdown, as indicated by the centerline fuel temperatures at the end of blowdown: 1067 F, 1013 F, and 1019 F for the  $C_d = 1.0$ , 0.8, and 0.6 cases, respectively. All three cases leave slightly over 50 cubic feet of liquid in the reactor vessel at the end of blowdown, making the adiabatic heatup periods during lower head refilling nearly the same. The reflooding transients are almost identical for the three cases, except that the 0.6 case floods slightly faster from 50 to 90 seconds, and thereafter, floods at the same rate as the other two cases, having built up a higher core liquid level.

The difference in peak cladding temperatures between the  $C_d = 1.0$  and the  $C_d = 0.8$  cases is due primarily to the better blowdown cooling in the latter case. The difference in centerline fuel temperatures at the end of blowdown, 54 F, is preserved through the remainder of the calculations, creating a 50 F difference in cladding temperature at the time of the peak. The  $C_d = 0.6$  case experiences the better blowdown cooling, being 48 F cooler than the  $C_d = 1.0$  case at the end of blowdown, but it also experiences extra flooding between 50 and 90 seconds. As can be seen in the cladding temperature plots, all three of the cases experience an increase in cooling, caused by the occurrence of rupture in the adjacent upstream node, at about 80 or 90 seconds. Because the  $C_d = 1.0$  and the  $C_d = 0.8$  cases have very similar flooding histories, this effect is nearly the same for both. For the  $C_d = 0.6$  case, the higher flooding rates near the rupture time cause the effect to be more pronounced. The resultant temperature difference, 50 to 60 F, remains throughout the rest of the evaluation, placing the  $C_d = 0.6$  case 60 F cooler than the  $C_d = 0.8$  case at the time of peak cladding temperature and 110 F cooler than the  $C_d = 1.0$  case.

The peak cladding temperatures were all for nodes that were adjacent to the rupture location. These evaluations were conducted

with only the B<sup>-</sup> 2H spacer grid and rupture modifications described in Chapter 5; thus, the final cladding temperature results were calculated in FRAP-T6. As such, there was no axial conduction allowed along the fuel pin. With axial conduction, the peak cladding temperature location may move to the grid span above the location of peak power. To be sure that the conclusions of the study remain valid for the complete set of evaluation model changes, a comparison of the cladding temperatures in the grid span above the ruptured span is required. The table below shows the peak cladding temperatures for the cladding in the non-grid nodes one span above the rupture location.

CASE	NODE 14	NODE 15
	PCT, F / Time, s	PCT, F / Time, s
Cd = 1.0	1683 / 321	1770 / 321
Cd = 0.8	1668 / 281	1761 / 378
Cd = 0.6	1676 / 352	1752 / 378

As can be seen, the differences in peak temperatures are compressed at the higher elevation and there is little difference in the results across the spectrum. From this study, it would appear to matter little which case was used for the remaining evaluations. Still, the Cd = 1.0 is slightly worse than the others and it is more than only marginally worse at the lower elevations. Therefore, the Cd = 1.0 was selected for the remaining sensitivity studies and as the worst case for the LOCA limits calculations with the final evaluation model.

#### 7.4 Break Type

Following the selection of the discharge coefficient, the type of break, split or guillotine, was considered. The guillotine break is modeled as a complete severance of the pipe, allowing separate discharges through the full area of the cold leg piping from both

the reactor vessel and pump sides of the break location. No mixing of the flows from the two sides of the break is allowed. The split break assumes discharge from the cold leg piping through an area twice the size of the cold leg piping cross section. Although the flows from the two sides, RC pump and reactor vessel, must still pass through limiting pipe areas, they are allowed to mix at the break location. The blowdown rates and system flow splits are somewhat different for the two types of breaks, and that can lead to some differences in cooling response.

Figures 7-36 through 7-43 present the results of the split type break case. These should be compared to Figures 7-12 through 7-19 which are for the reference guillotine case. Both are double-area breaks with  $C_d = 1.0$  and located at the pump discharge. Key data from the split case are included with the spectrum studies in Table 7-1. As can be seen, the period of blowdown for the split break is shorter by two seconds. Although the blowdown flows differ somewhat, the degree of cooling is essentially the same between the split and the guillotine, as shown by the centerline fuel temperature of the hot pin at the end of blowdown, 1007 F for the guillotine and 1061 F for the split. Similar to the  $C_d = 0.6$  guillotine break from the break size study, the flooding rate for the split is higher than for the guillotine between 70 and 100 seconds. Therefore, when rupture occurs, the interphase heat transfer increases at the ruptured node are more effective with the result that the cladding temperature increase is slowed substantially more for the split than for the guillotine. The temperature difference thus set up is preserved for the remainder of the run, resulting in the 59 F lower peak cladding temperature for the split case.

An examination of the results for the grid span just above the ruptured location verifies that the split should remain the less limiting case when the complete set of evaluation model techniques is applied.

<u>CASE</u>	<u>NODE 14</u>	<u>NODE 15</u>
	<u>PCT. F / Time, s</u>	<u>PCT. F / Time, s</u>
Guillotine	1633 / 321	1770 / 321
Split	1665 / 334	1750 / 365

As with the discharge coefficient study, the differences in results are almost marginal. Nonetheless, the double-ended break produces the higher peak temperatures, and on that basis, was selected as the worst configuration for the remaining sensitivity studies and for the LOCA limits calculations using the complete set of EM changes.

#### 7.5 Maximum ECCS Analysis

The final sensitivity study, prior to the LOCA limits studies, is to determine which condition for the ECCS is more severe, with or without a single failure. Under a single failure assumption, only one train of pumped ECCS injection is available. With no failure, two full trains are available. Because the sizing of each individual train must be sufficient to mitigate an accident, the second train is redundant relative to providing adequate water for core cooling. In an analysis, nearly all the extra injection capacity will spill from the primary coolant system to the containment where it may interact with the atmosphere to reduce the containment pressure. As the lowering of the containment pressure causes a reduction in core reflooding rate, the evaluation of a fully functional ECCS may actually show a higher peak cladding temperature than would be predicted using the single failure assumption.

The calculations presented thus far have all been performed assuming a single active failure. The assumed failure, that of the diesel generator emergency power supply, results in a loss of

electrical power to half of the pumped ECC systems. This is generally referred to as a "minimum ECCS" case. To evaluate the assumption of no failure, a calculation was made with all of the ECCS functional, the "maximum ECCS" case. Figures 7-44 through 7-59 present relevant parameters for the maximum ECCS case. These should be compared to the guillotine  $C_d = 1.0$  from the discharge coefficient study, Figures 7-12 through 7-19.

The extra ECC available in the maximum ECCS case has two effects during reflood: First, the ECCS water that is injected into the intact loops condenses more of the steam flowing through the loops and lowers the RCS pressure. Second, the ECCS water that is spilled into the containment mixes with the containment atmosphere and reduces its pressure. The reduced containment pressure in turn reduces the RCS pressure. The effect of reducing the RCS pressure is to increase the specific volume of the steam created during core reflooding. As a result, the steam is more difficult to vent through the system, and the core flooding rate decreases. A comparison of Figures 7-50 and 7-14 shows the flooding rate reduction for the McGuire/Catawba model. As there are no blowdown cooling benefits to compensate for the lower flooding rate, the cladding temperature increases by 82 F as shown in Figures 7-54 and 7-16. An examination of the cladding temperatures one grid span above the rupture location shows that the cladding temperature is increased at that location as well. Therefore, the conclusion that the peak cladding temperature will be higher for the maximum ECCS case will also apply for the analyses done with the final evaluation model.

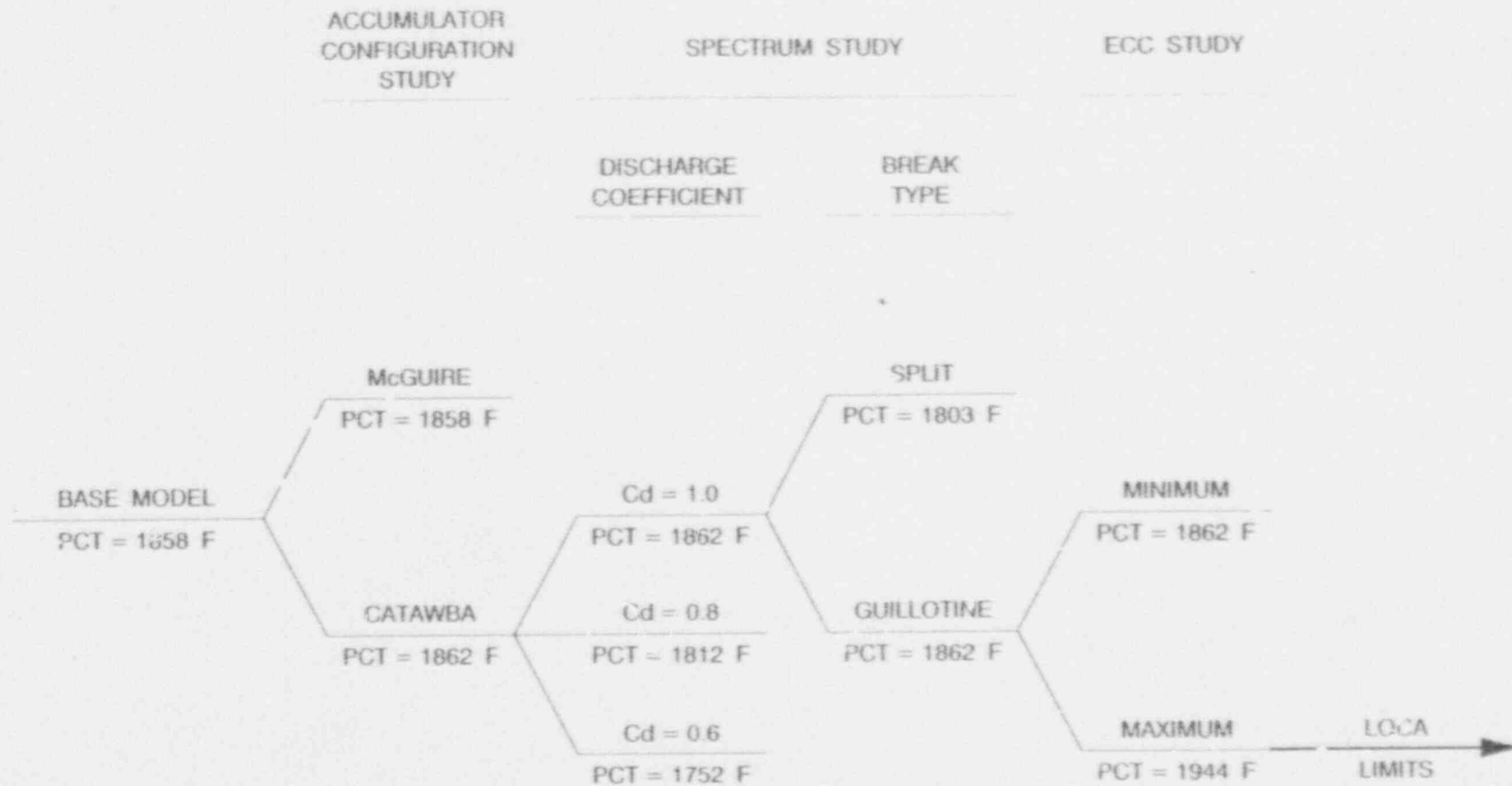


Table 7-1 Spectrum and Break Type Comparison

Item or Parameter	Cd =>	Guillotines			Split
		1.0	0.8	0.6	1.0
End of slowdown, s		22.3	23.6	29.0	20.3
Liquid in Reactor Vessel at EOB, ft <sup>3</sup>		57.4	72.3	69.1	64.8
Bottom-of-Core Recovery, s		35.2	36.2	42.6	32.7
Time of Rupture, s		74.4	78.6	90.4	73.5
Ruptured Node *		11	11	11	11
PCT at Rupture Node, F		1613	1597	1597	1600
Node Adjacent to Rupture *		12	12	12	12
PCT of Adjacent Node, F		1862	1812	1750	1803
Node in Adjacent Grid Span *		15	15	15	15
PCT of Adjacent Grid Span, F		1770	1761	1752	1750
Pin PCT Node *		12	12	15	12

\* Refer to Figure 4-4 for noding arrangement

# FIGURE 7-1 PLANT SPECIFIC STUDIES ANALYSIS DIAGRAM



BASE MODEL: 2A/G @ PD Cd = 1.0

FIGURE 7-2 SENSITIVITY STUDY - BASE MODEL  
SYSTEM PRESSURE DURING BLOWDOWN

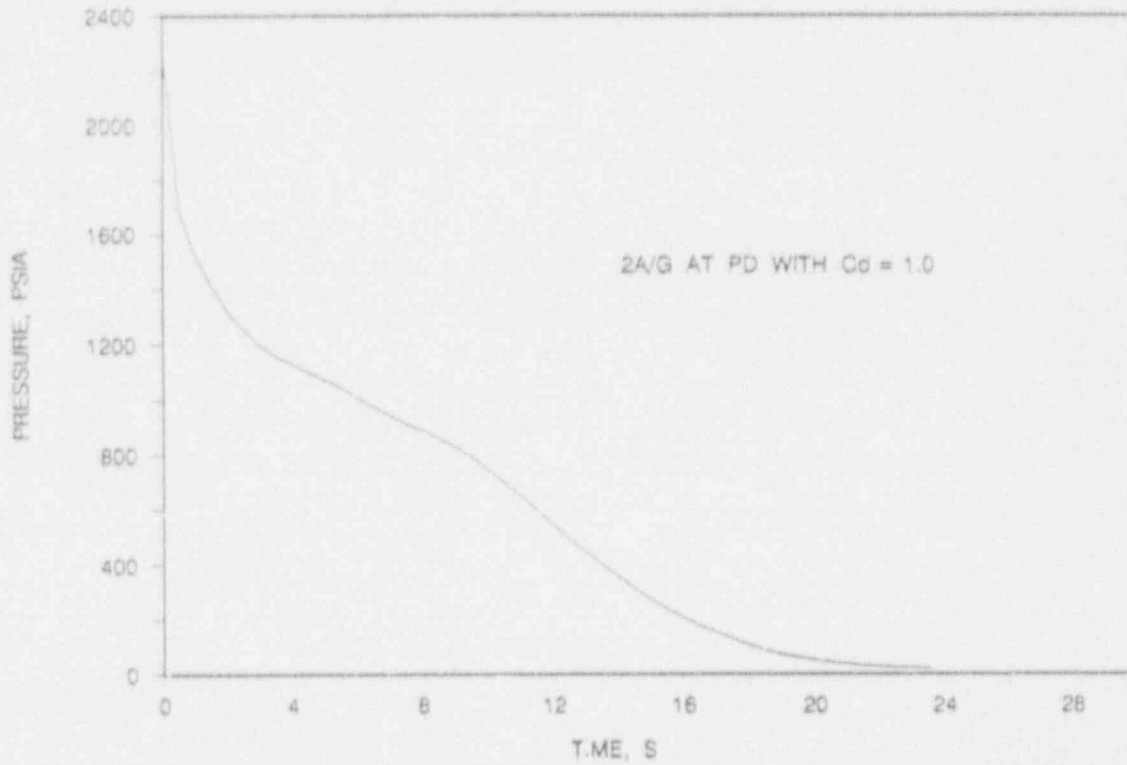


FIGURE 7-3 SENSITIVITY STUDY - BASE MODEL  
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

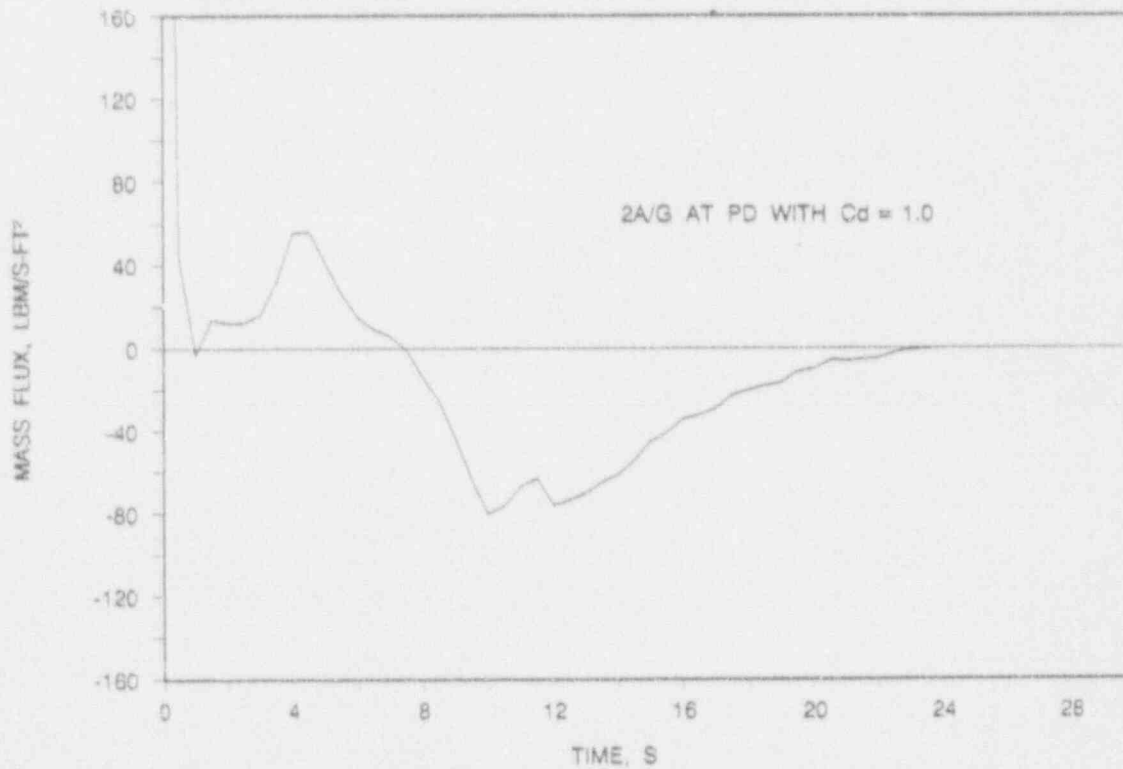


FIGURE 7-4 SENSITIVITY STUDY - BASE MODEL  
REFLOODING RATE

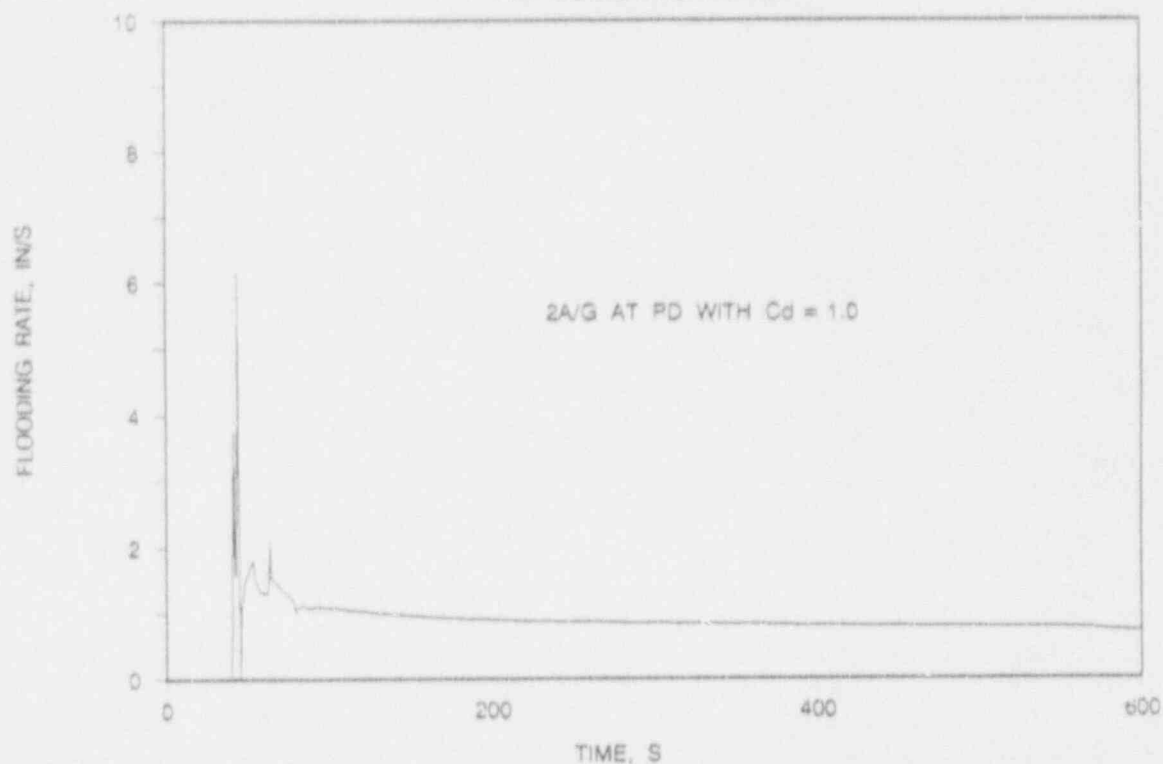


FIGURE 7-5 SENSITIVITY STUDY - BASE MODEL  
HEAT TRANSFER COEFFICIENT AT PEAK POWER LOCATION

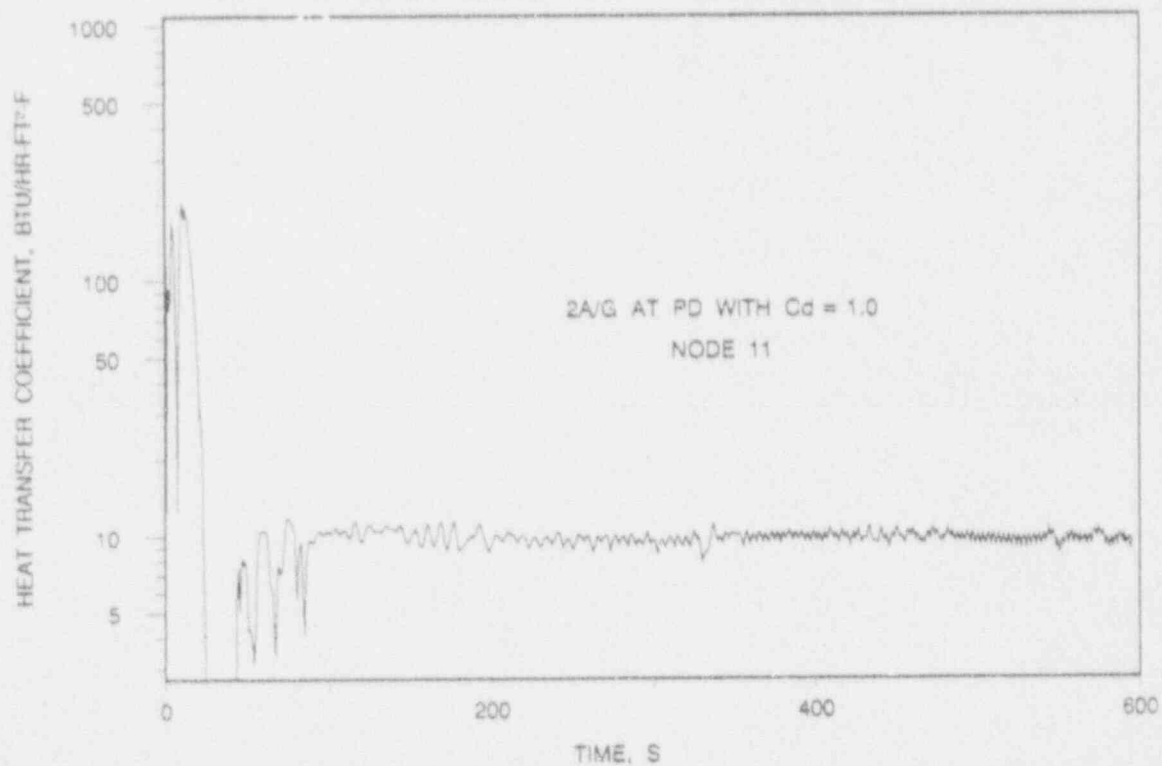


FIGURE 7-6 SENSITIVITY STUDY - BASE MODEL  
PEAK CLADDING TEMPERATURE

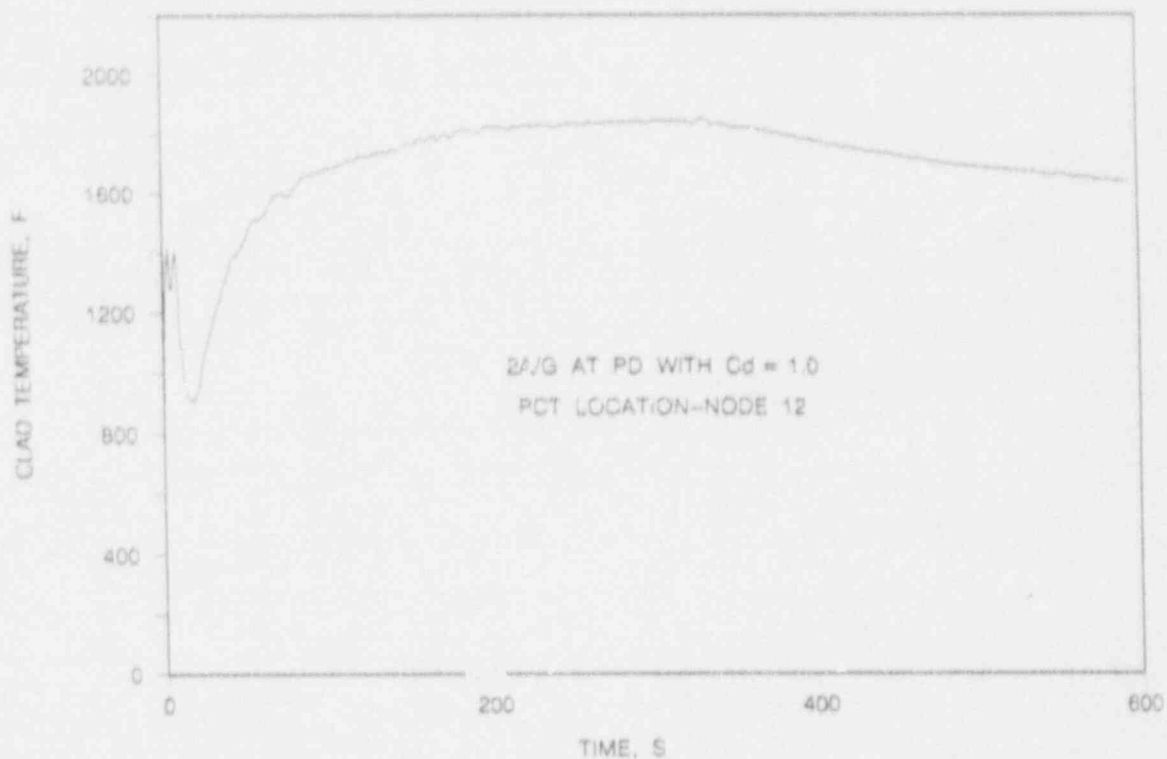


FIGURE 7-7 SENSITIVITY STUDY - BASE MODEL  
CLADDING TEMPERATURE AT RUPTURE LOCATION

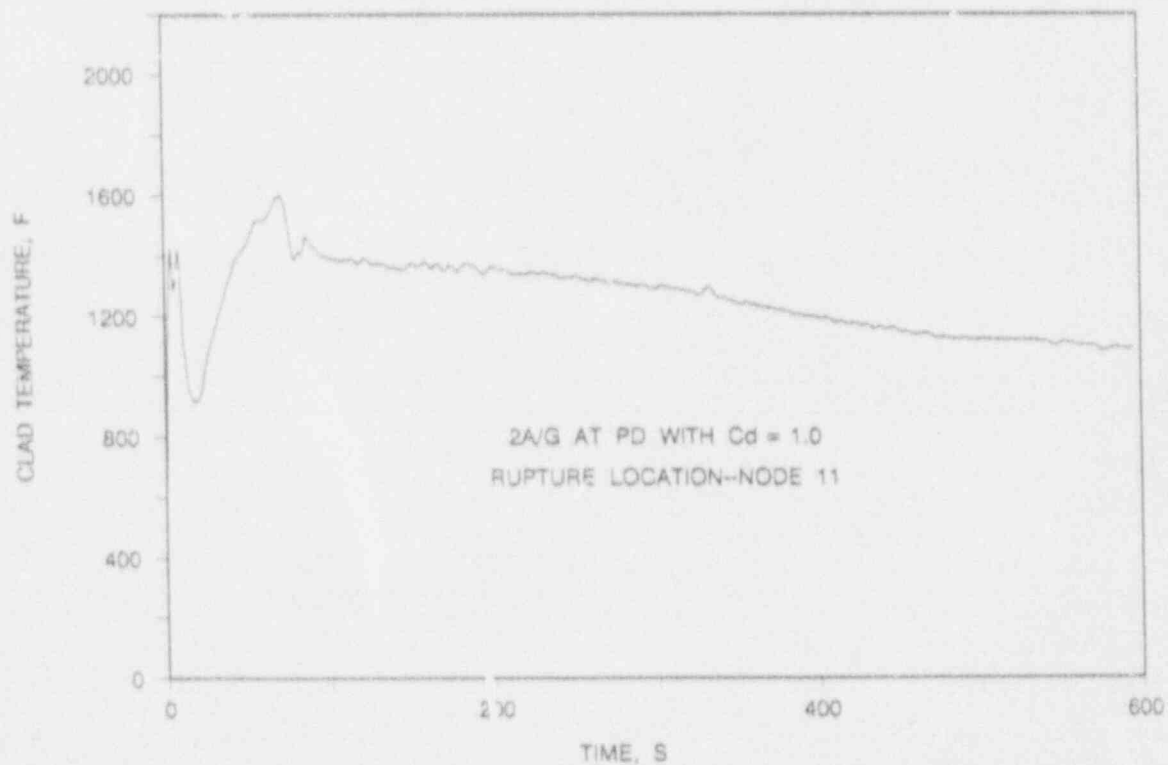


FIGURE 7-8 SENSITIVITY STUDY - BASE MODEL  
CLADDING TEMPERATURE IN ADJACENT GRID SPAN

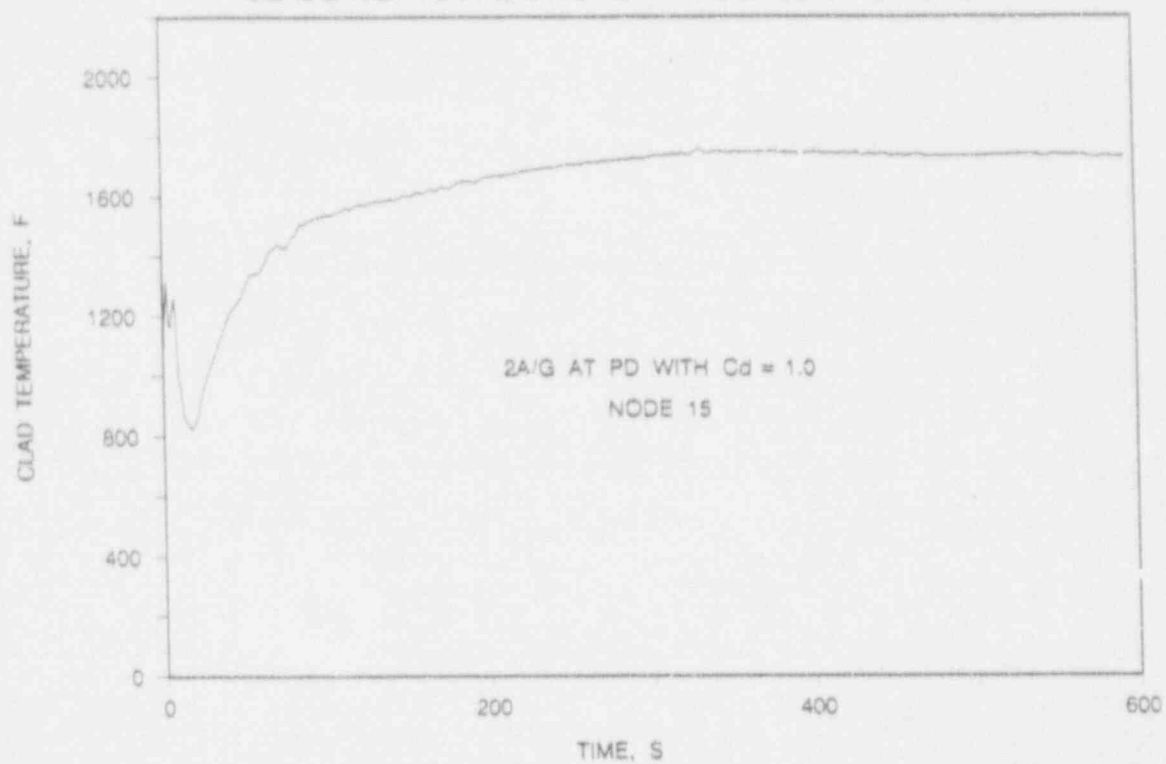


FIGURE 7-9a SENSITIVITY STUDY - BASE MODEL  
FLUID TEMPERATURE AT PCT LOCATION

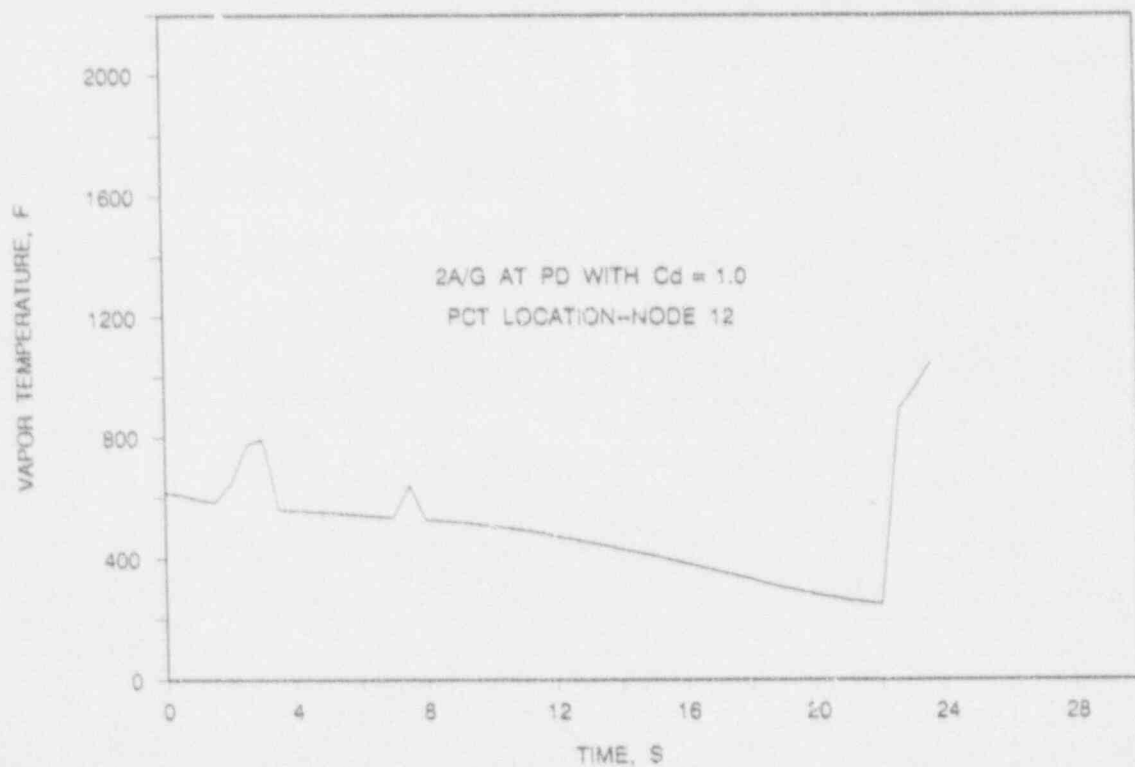




FIGURE 7-9b SENSITIVITY STUDY - BASE MODEL  
FLUID TEMPERATURE AT PCT LOCATION

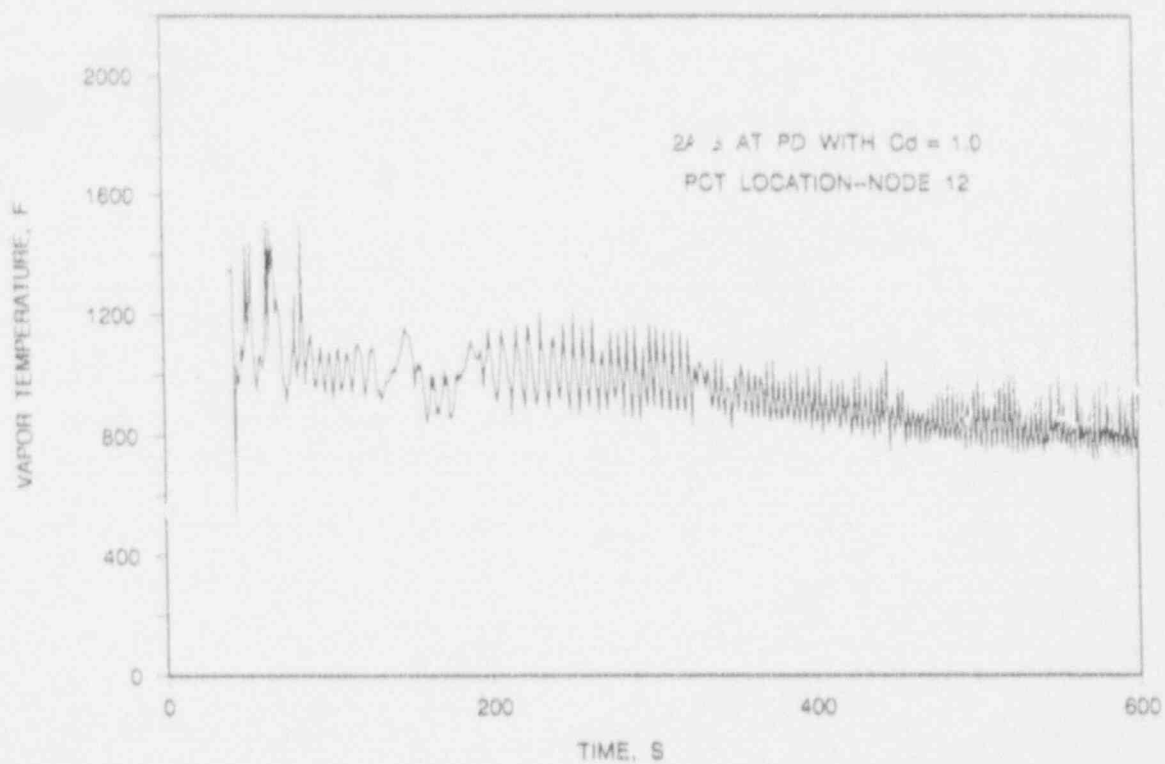


FIGURE 7-10 ACCUMULATOR STUDY - McGUIRE vs CATAWBA  
ACCUMULATOR FLOW RATES

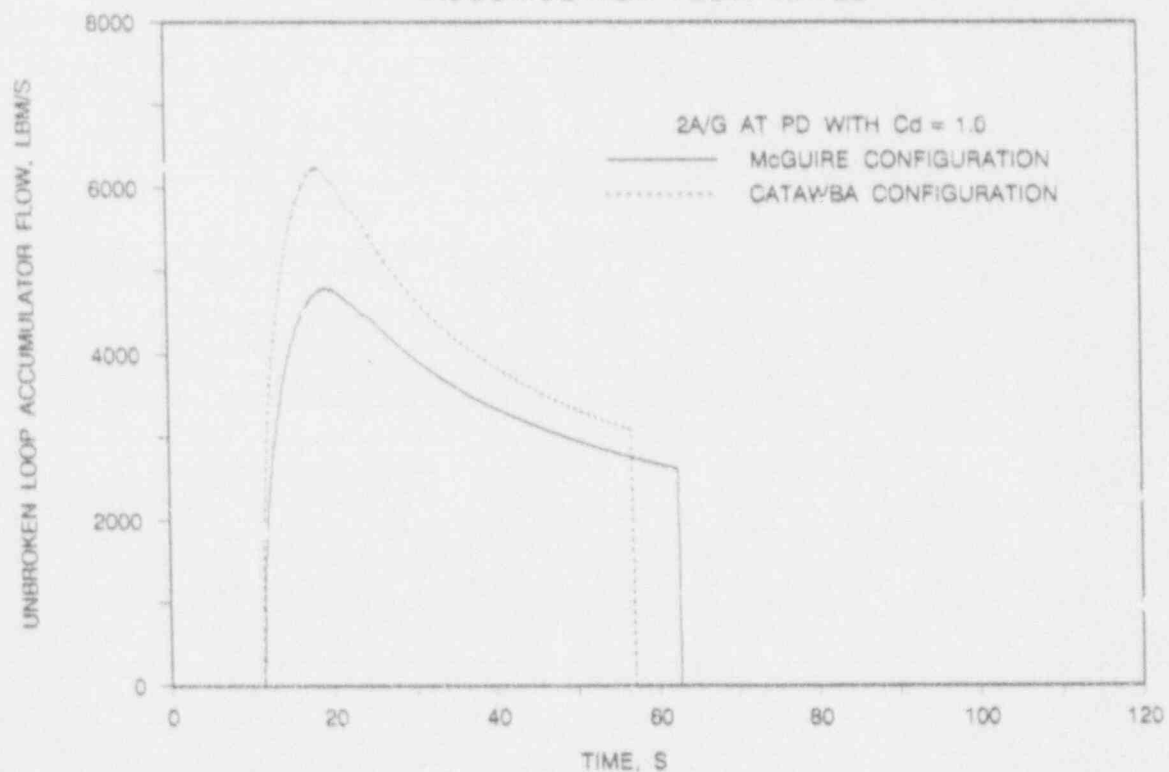


FIGURE 7-11 ACCUMULATOR STUDY - McGUIRE vs CATAWBA  
PEAK CLADDING TEMPERATURES

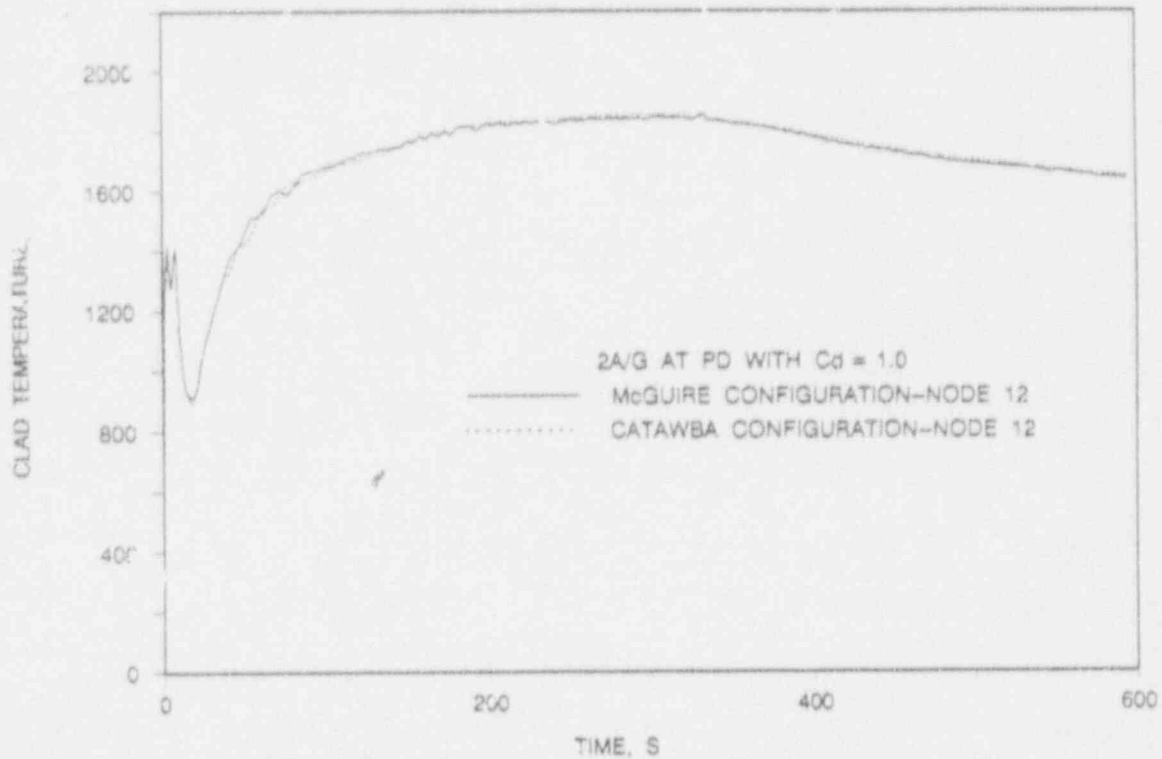


FIGURE 7-12 DISCHARGE COEFFICIENT STUDY -  $C_d = 1.0$   
SYSTEM PRESSURE DURING BLOWDOWN

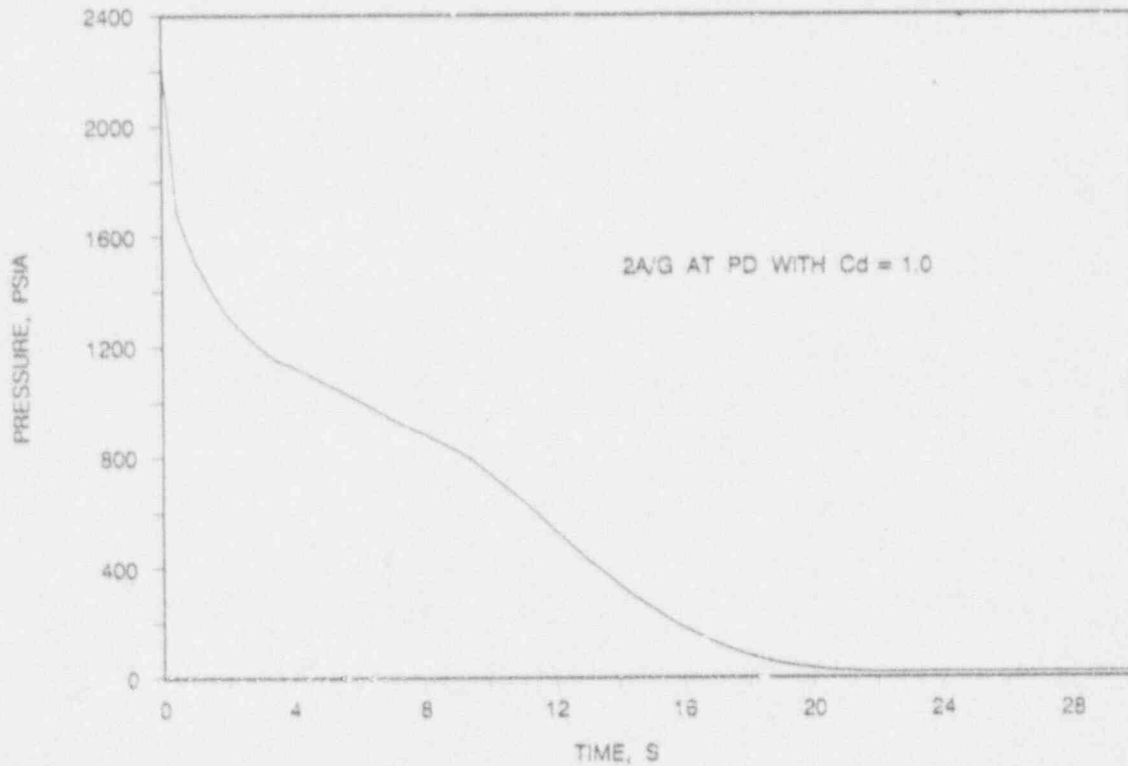


FIGURE 7-13 DISCHARGE COEFFICIENT STUDY -  $C_d = 1.0$   
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

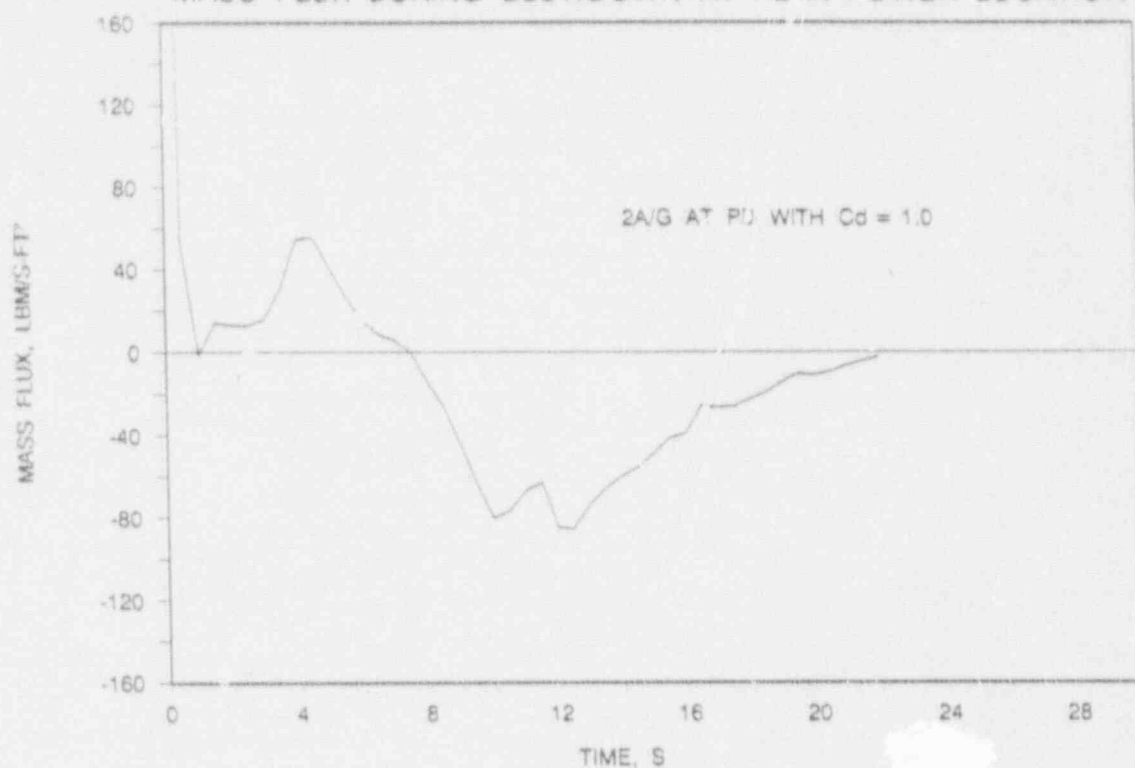


FIGURE 7-14 DISCHARGE COEFFICIENT STUDY -  $C_d = 1.0$   
REFLOODING RATE

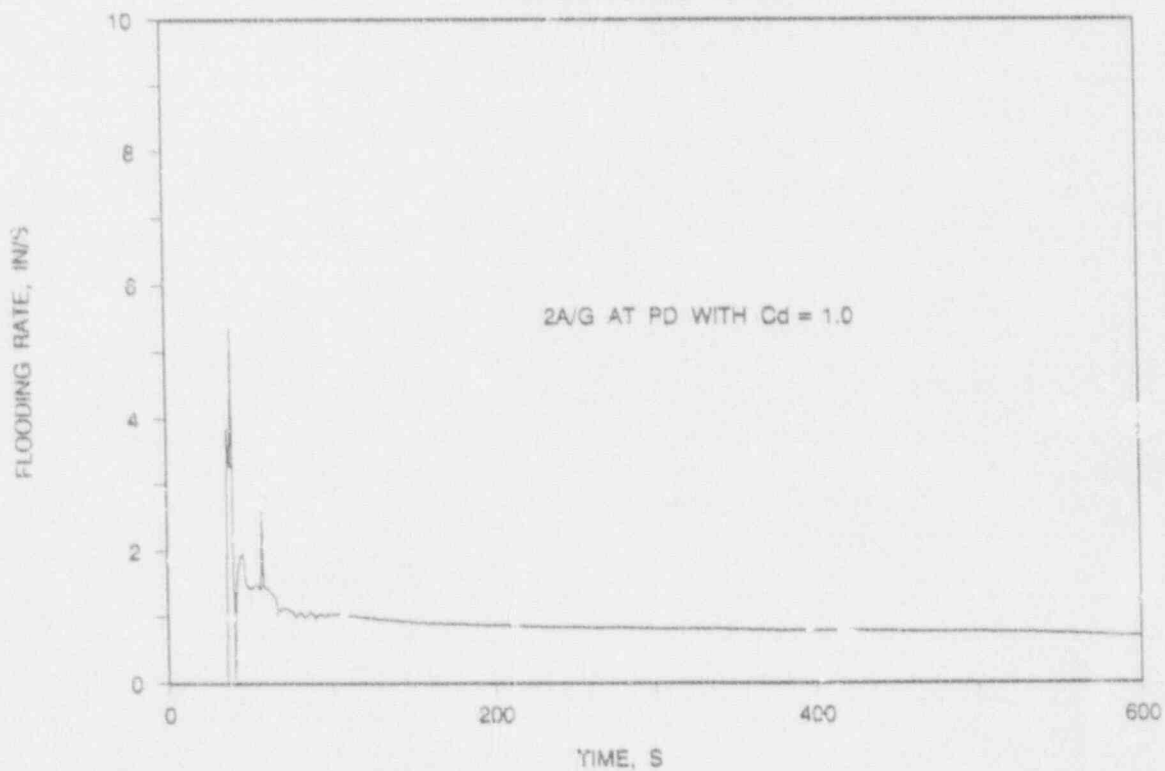


FIGURE 7-15 DISCHARGE COEFFICIENT STUDY -  $C_d = 1.0$   
HEAT TRANSFER COEFFICIENT AT PEAK POWER LOCATION

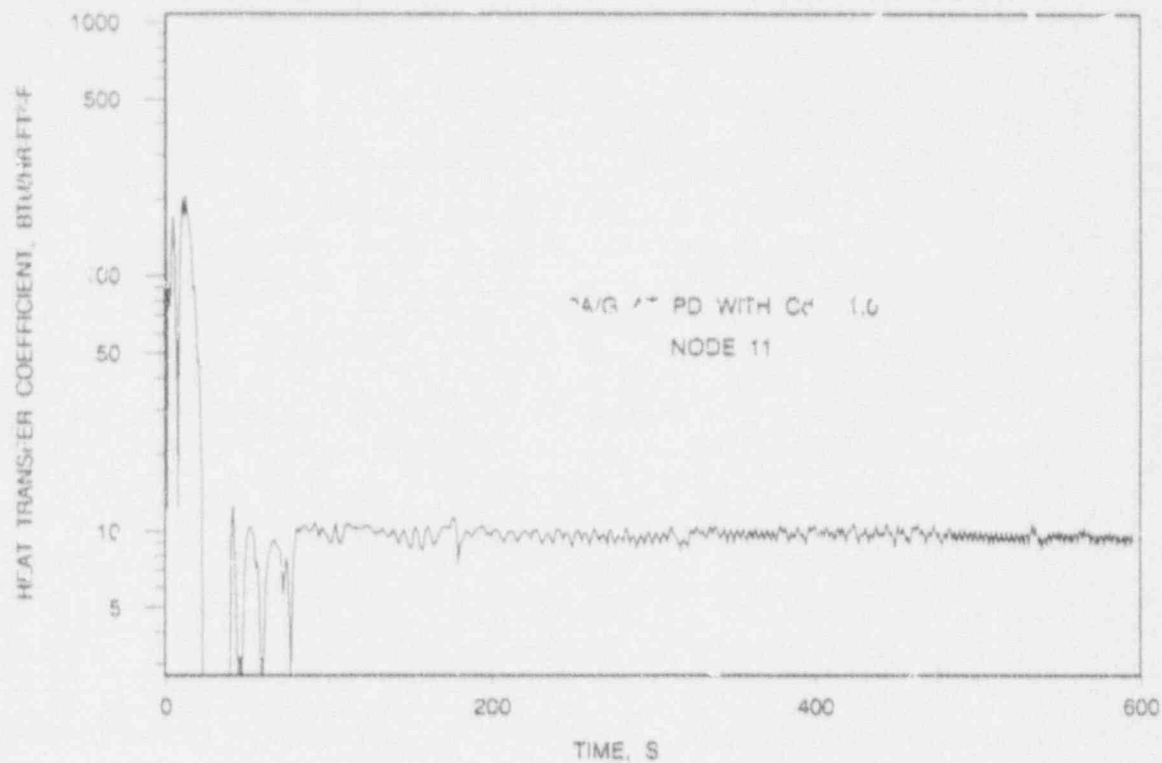


FIGURE 7-16 DISCHARGE COEFFICIENT STUDY -  $C_d = 1.0$   
PEAK CLADDING TEMPERATURE

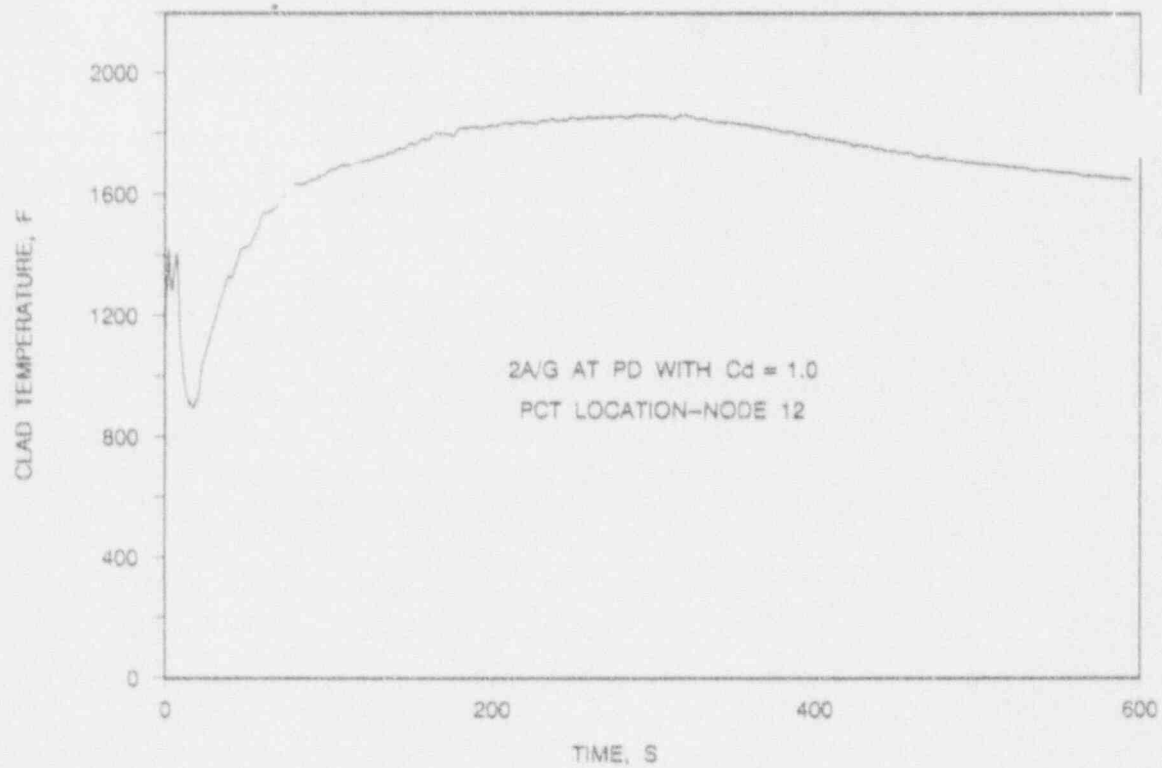


FIGURE 7-17 DISCHARGE COEFFICIENT STUDY -  $C_d = 1.0$   
CLADDING TEMPERATURE AT RUPTURE LOCATION

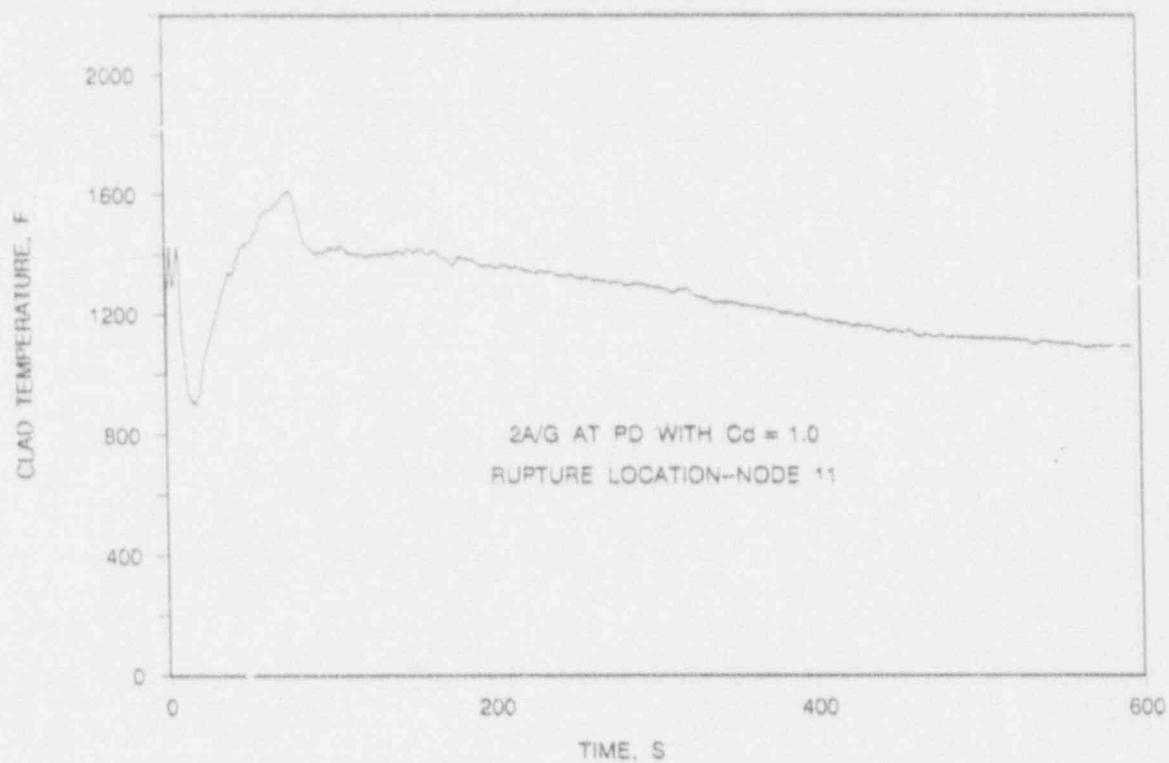


FIGURE 7-18 DISCHARGE COEFFICIENT STUDY -  $C_d = 1.0$   
CLADDING TEMPERATURE IN ADJACENT GRID SPAN

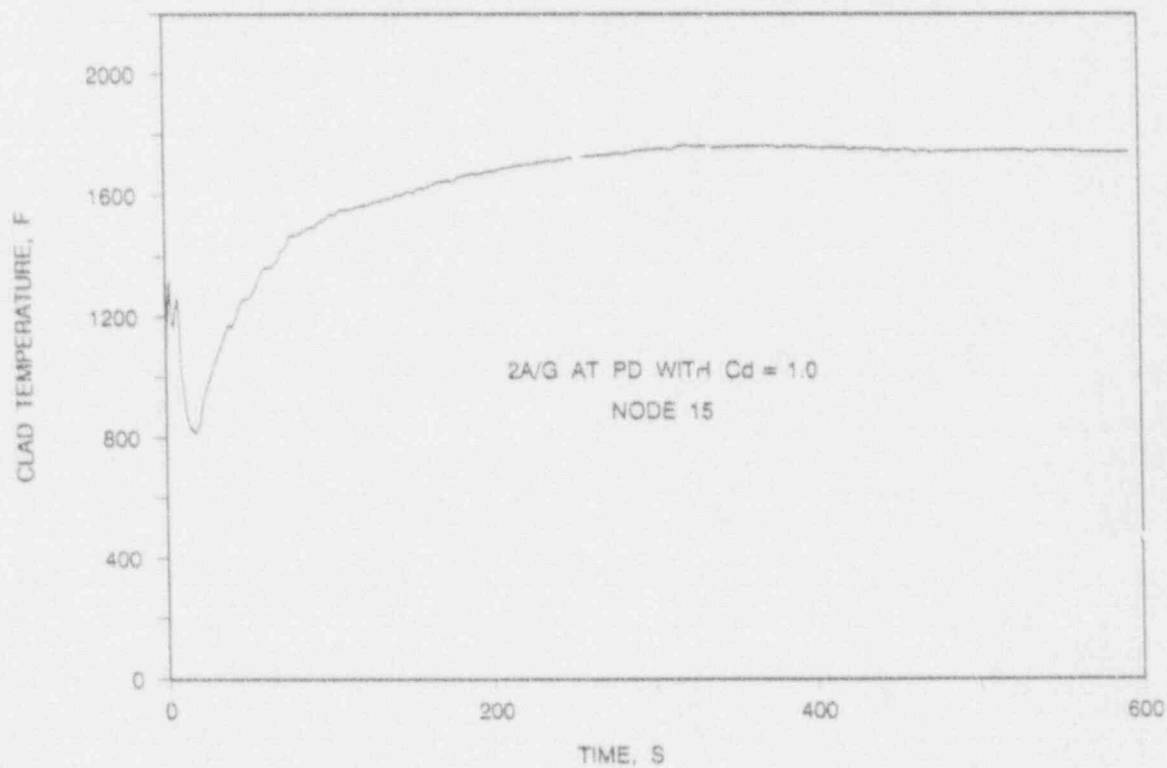


FIGURE 7-19a DISCHARGE COEFFICIENT STUDY -  $C_d = 1.0$   
FLUID TEMPERATURE AT PCT LOCATION

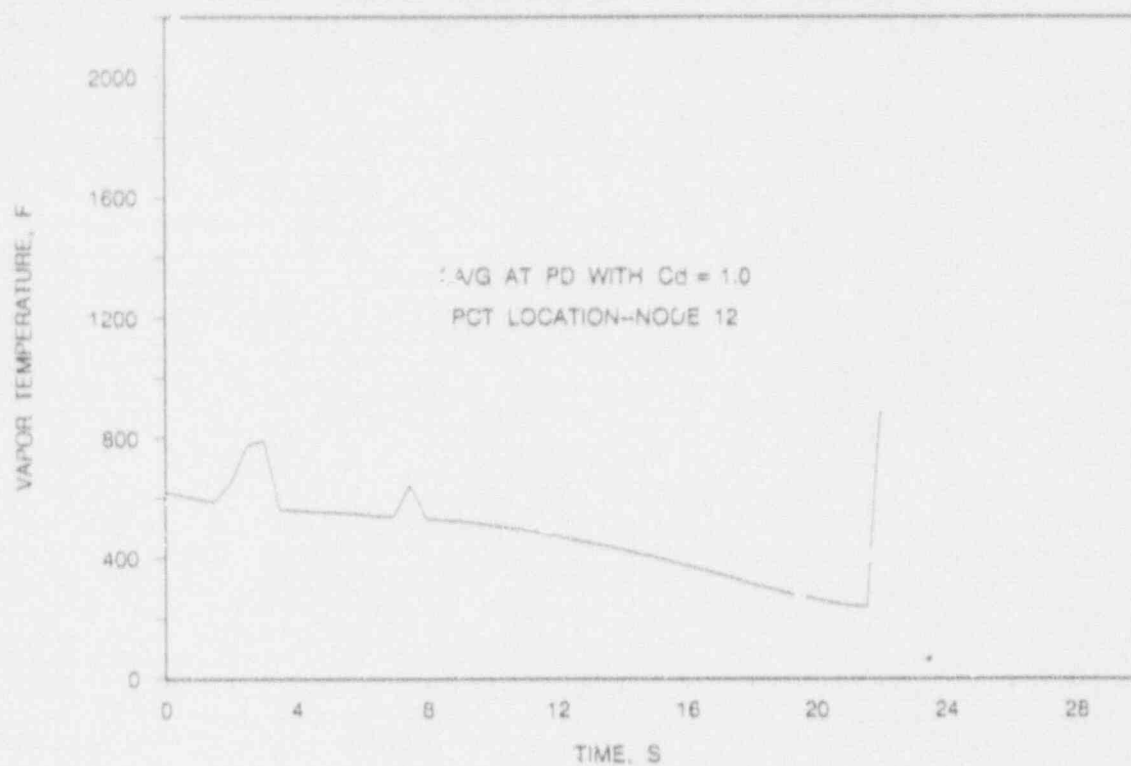


FIGURE 7-19b DISCHARGE COEFFICIENT STUDY -  $C_d = 1.0$   
FLUID TEMPERATURE AT PCT LOCATION

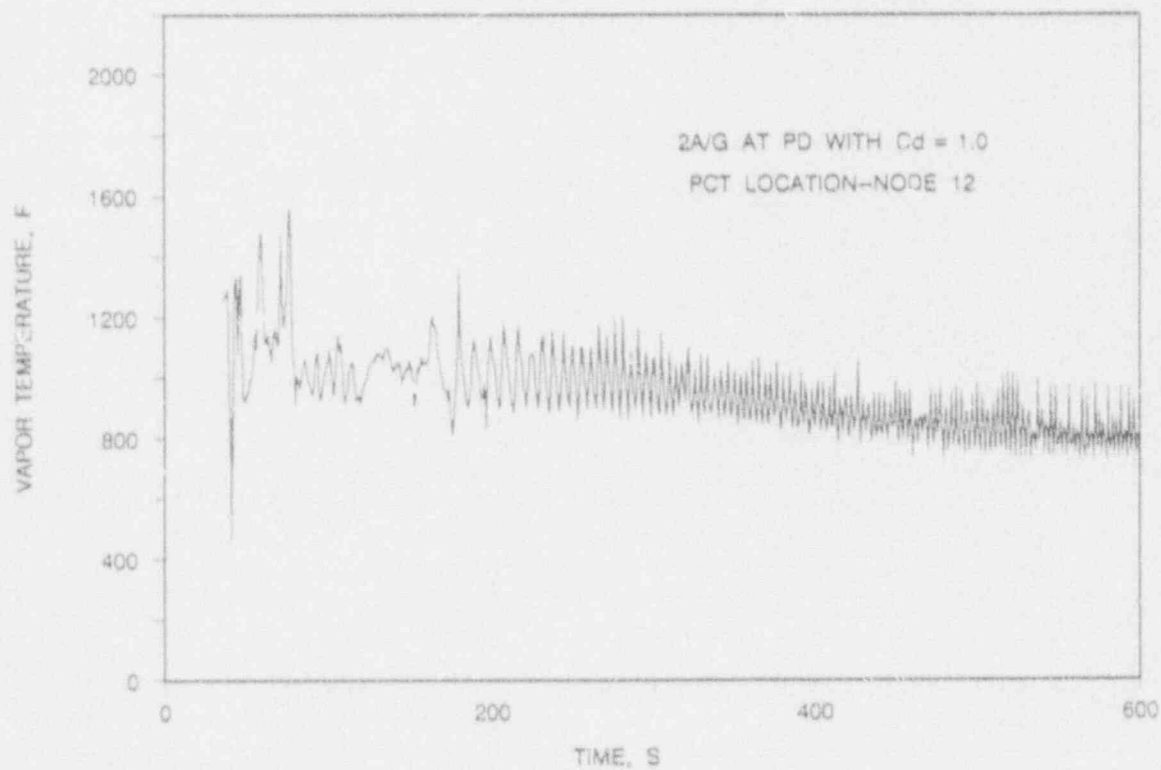




FIGURE 7-20 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.8$   
SYSTEM PRESSURE DURING BLOWDOWN

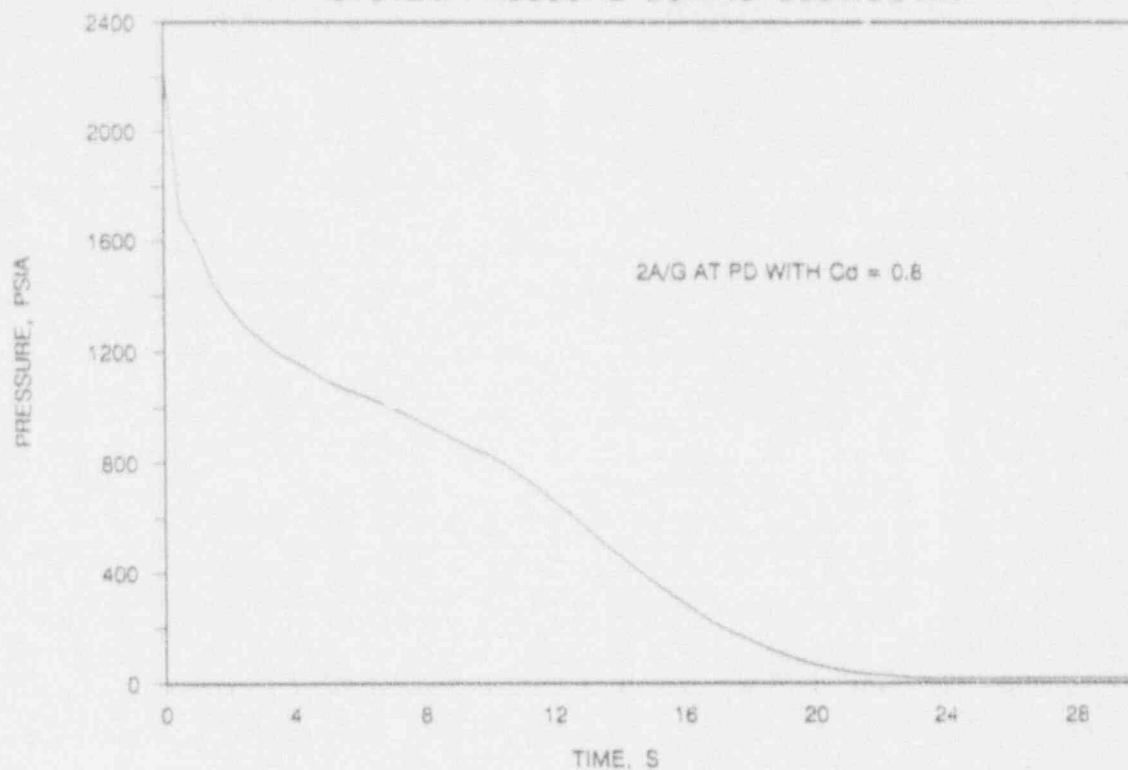


FIGURE 7-21 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.8$   
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

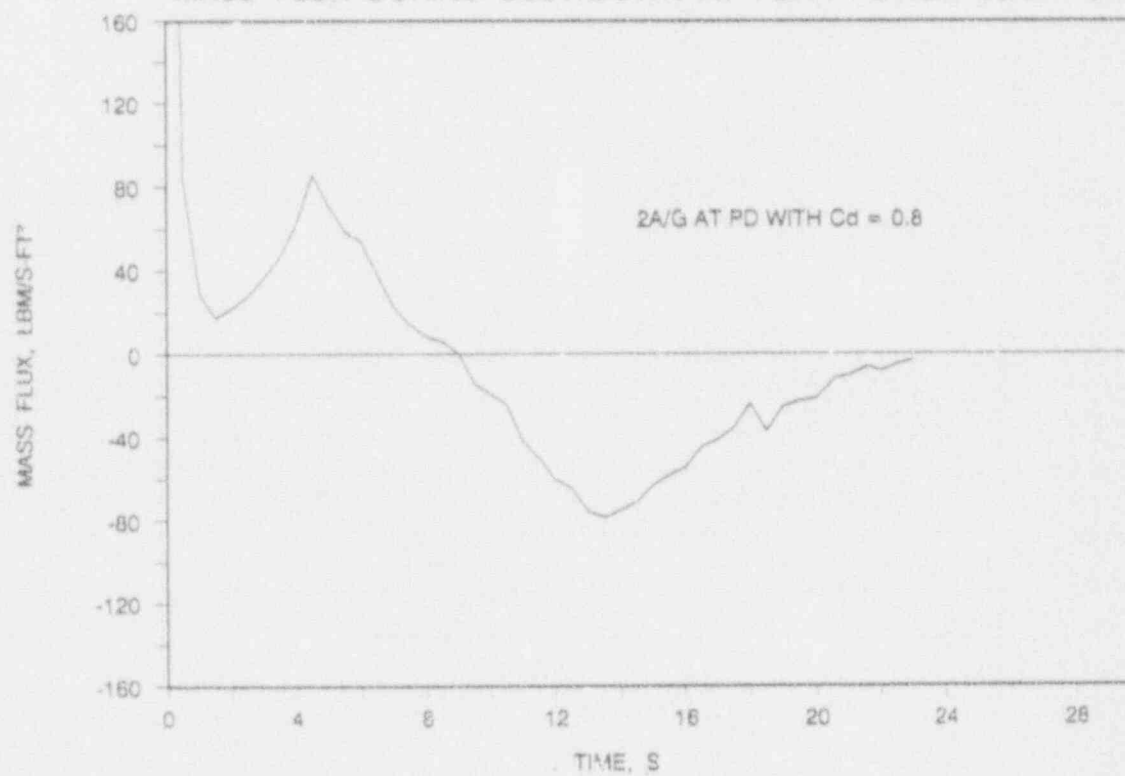


FIGURE 7-22 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.8$   
REFLOODING RATE

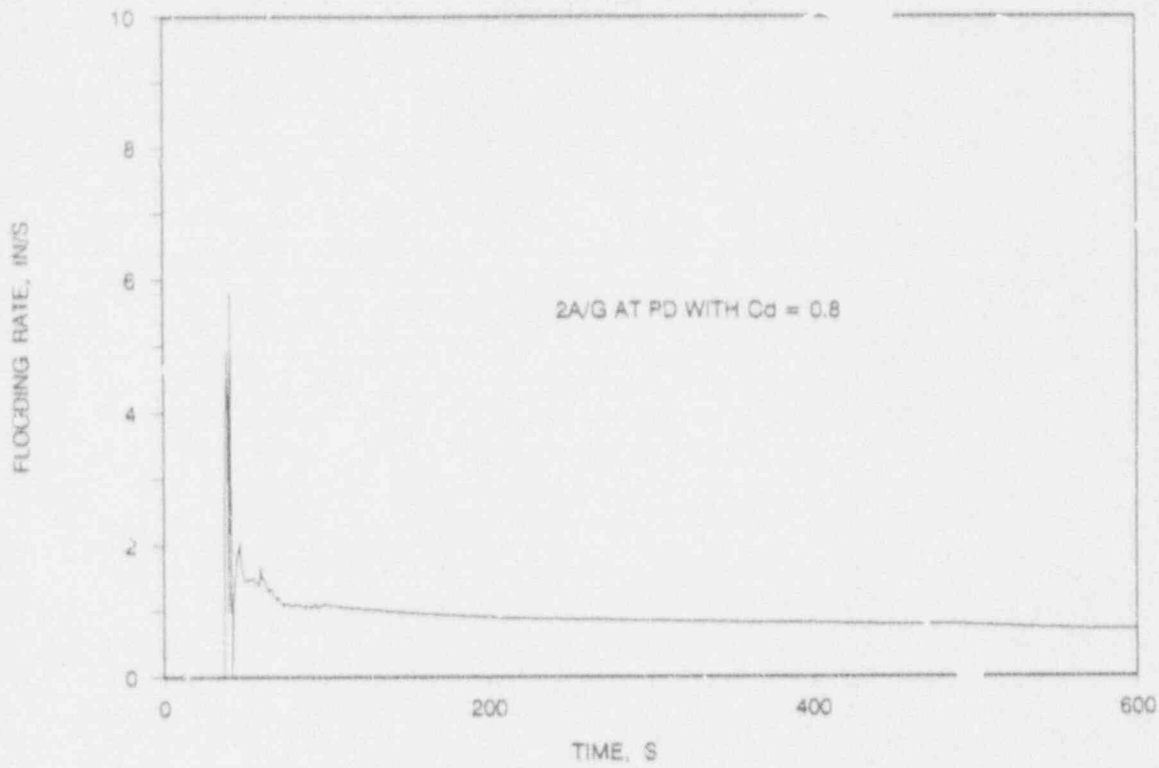


FIGURE 7-23 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.8$   
HEAT TRANSFER COEFFICIENT AT PEAK POWER LOCATION

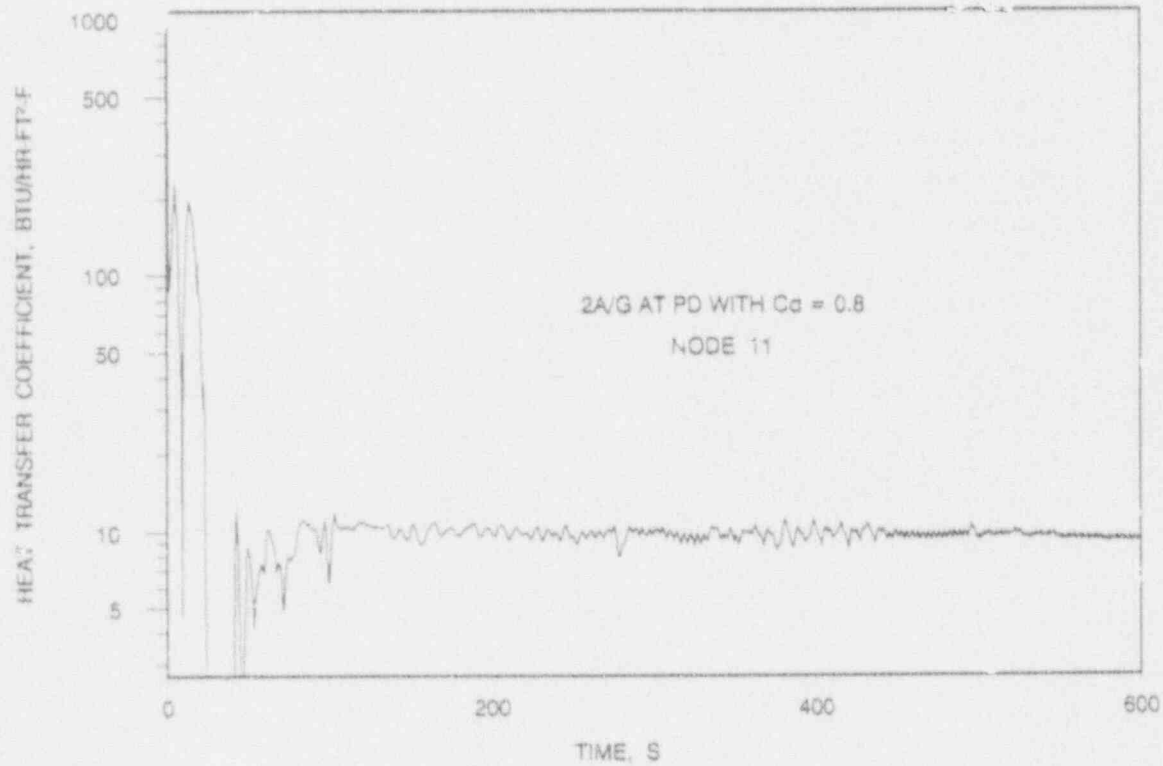


FIGURE 7-24 DISCHARGE COEFFICIENT STUDY -  $C_d \approx 0.8$   
PEAK CLADDING TEMPERATURE

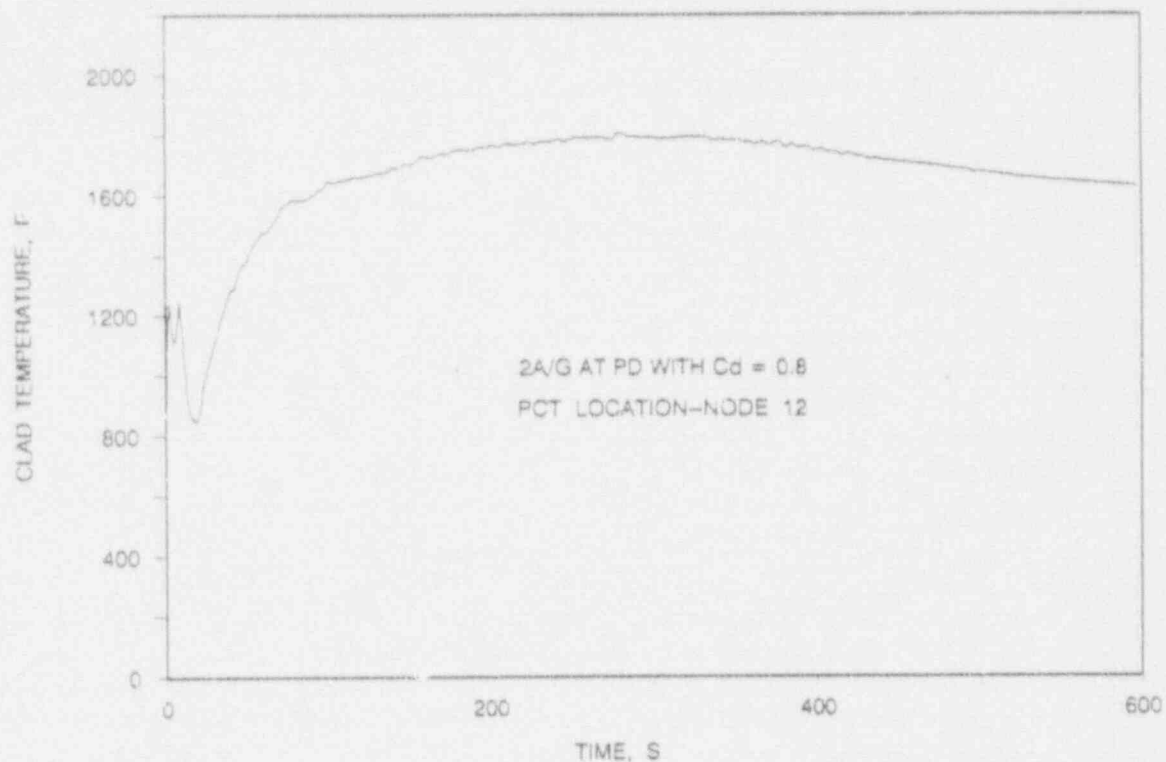


FIGURE 7-25 DISCHARGE COEFFICIENT STUDY -  $C_d \approx 0.8$   
CLADDING TEMPERATURE AT RUPTURE LOCATION

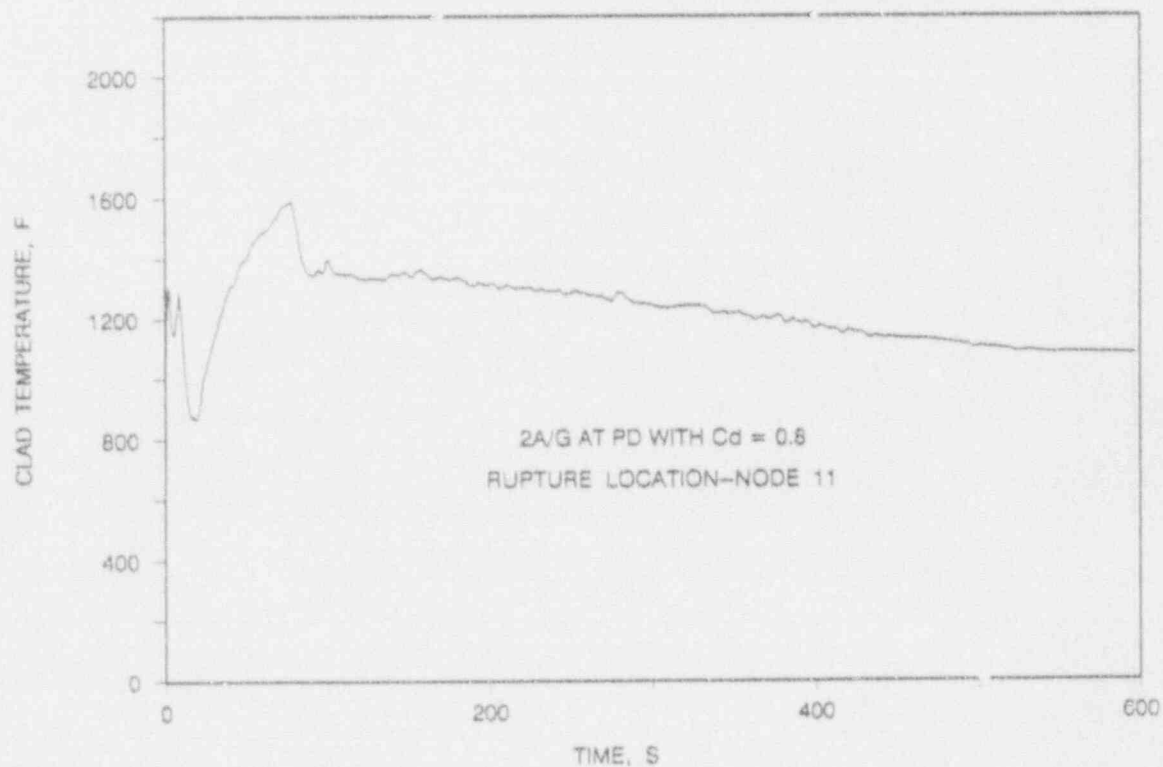


FIGURE 7-26 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.8$   
CLADDING TEMPERATURE IN ADJACENT GRID SPAN

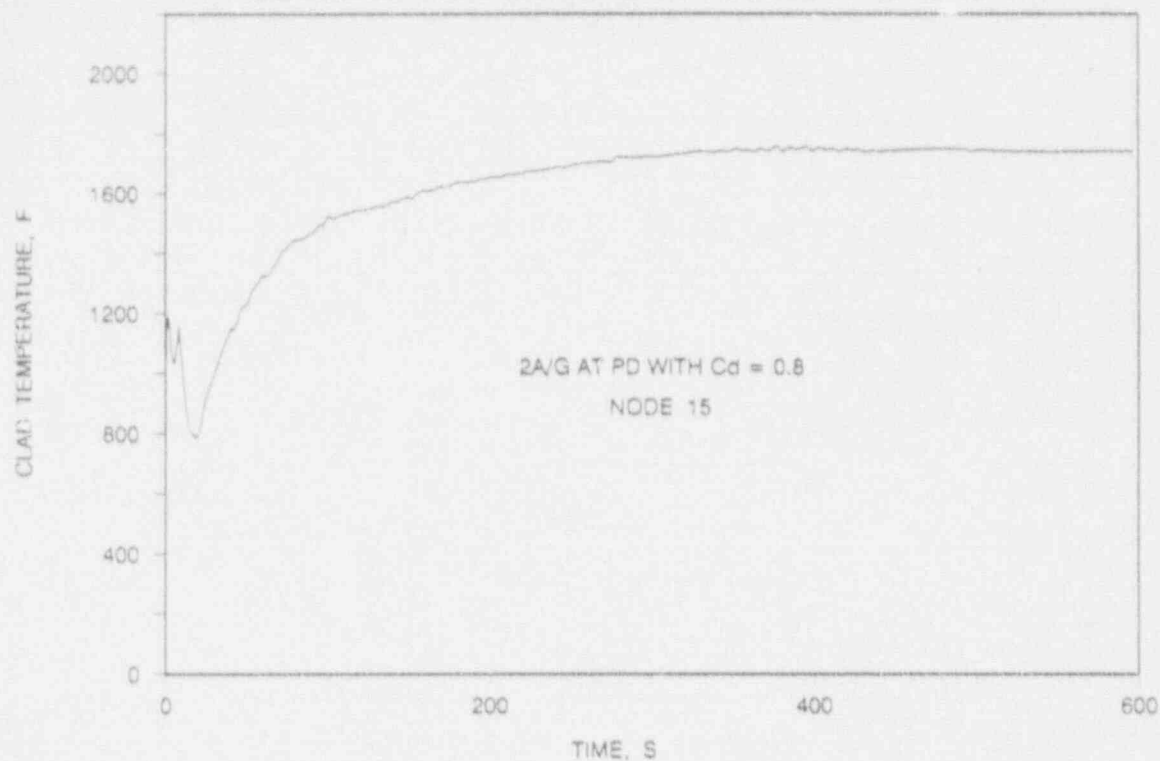


FIGURE 7-27a DISCHARGE COEFFICIENT STUDY -  $C_d = 0.8$   
FLUID TEMPERATURE AT PCT LOCATION

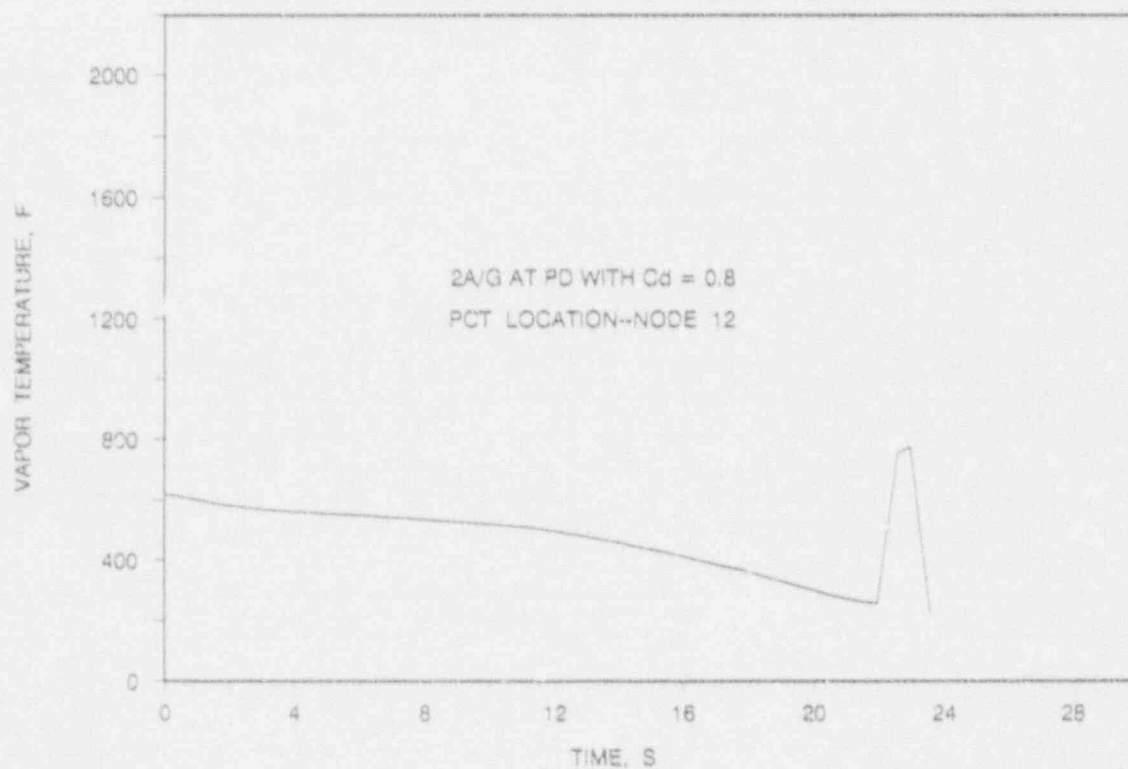


FIGURE 7-27b DISCHARGE COEFFICIENT STUDY -  $C_d = 0.8$   
FLUID TEMPERATURE AT PCT LOCATION

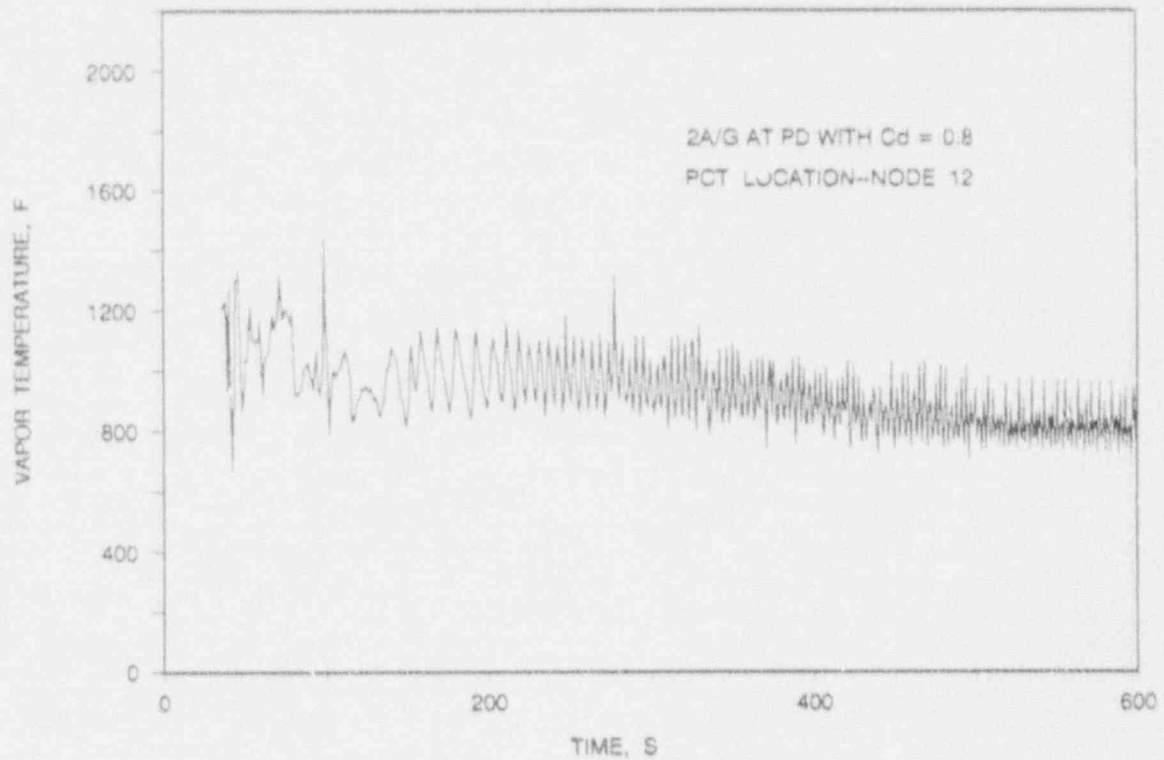


FIGURE 7-28 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.6$   
SYSTEM PRESSURE DURING BLOWDOWN

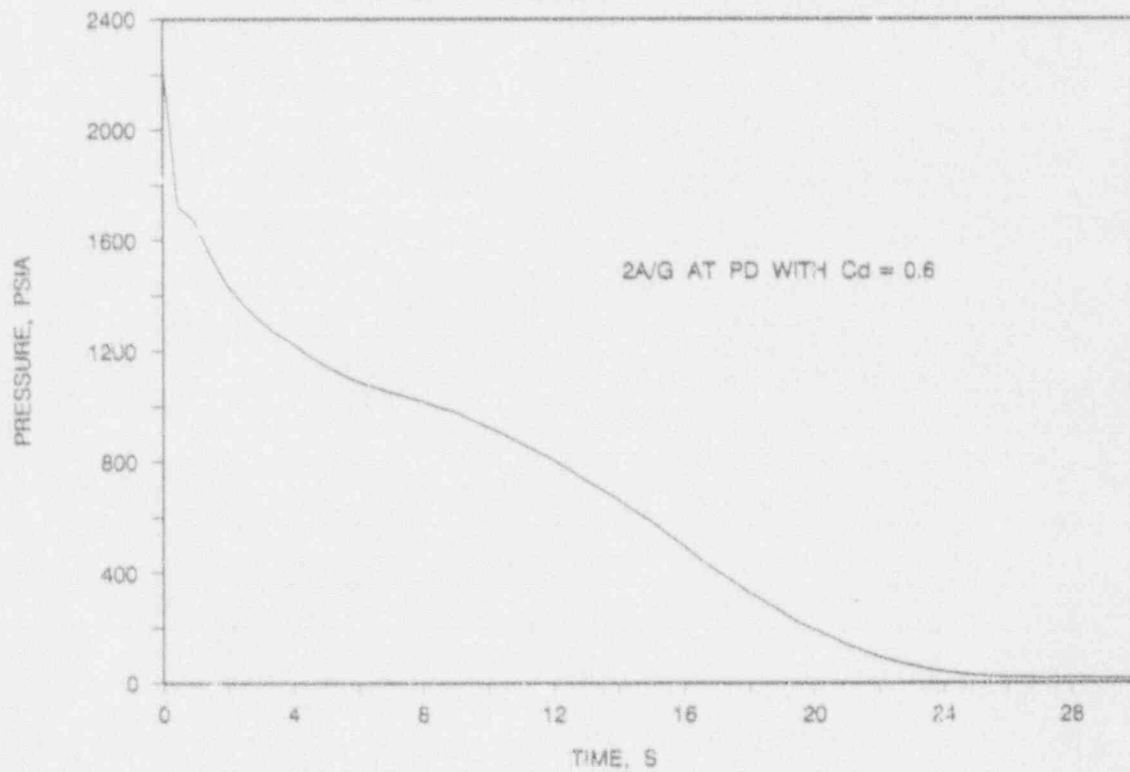


FIGURE 7-29 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.6$   
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

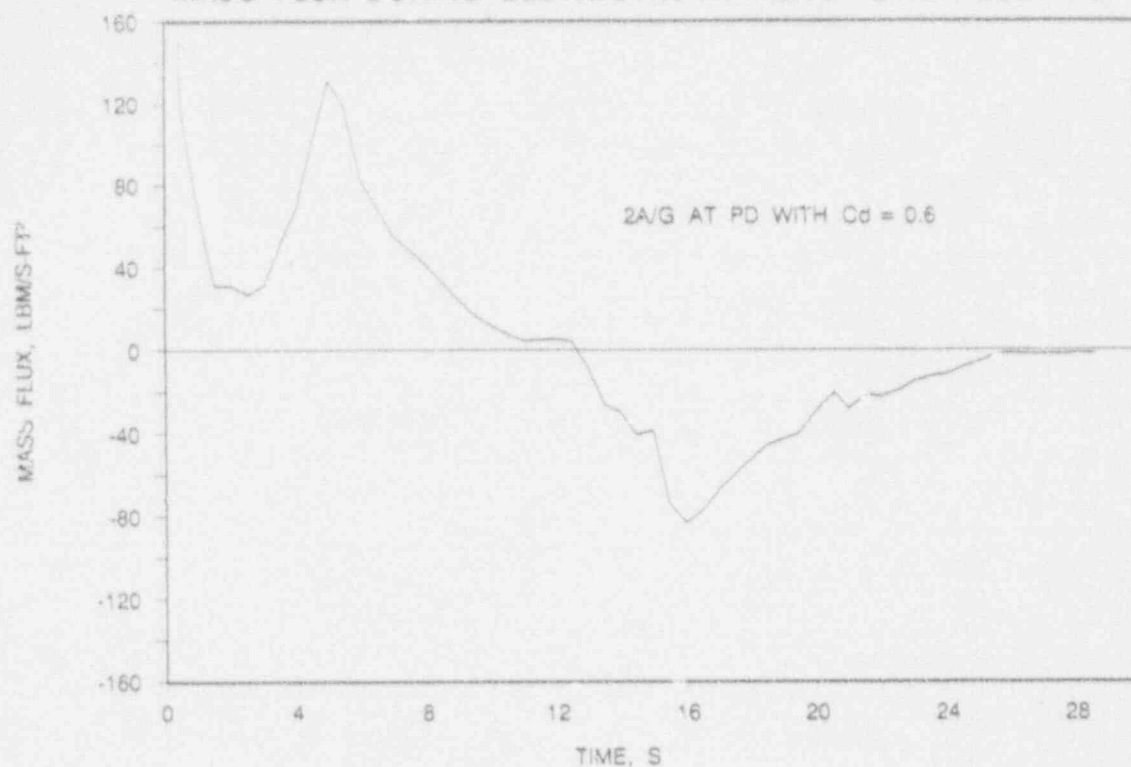


FIGURE 7-30 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.6$   
REFLOODING RATE

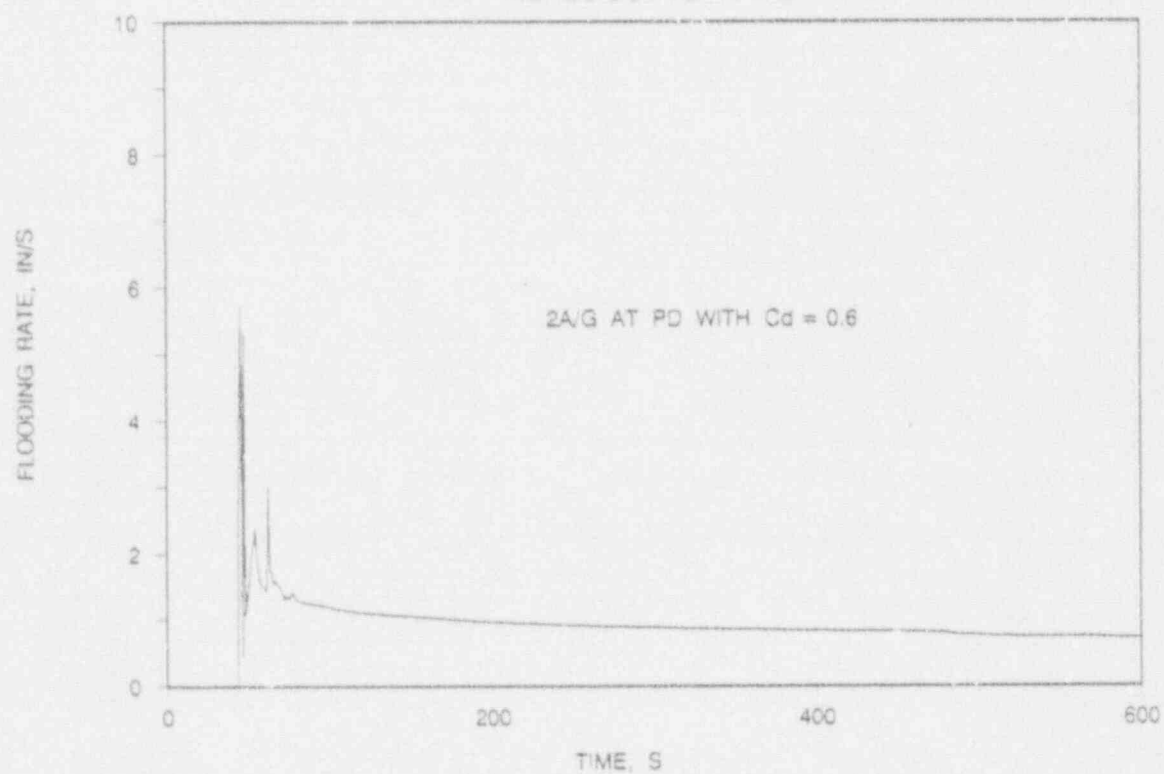




FIGURE 7-31 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.6$   
HEAT TRANSFER COEFFICIENT AT PEAK POWER LOCATION

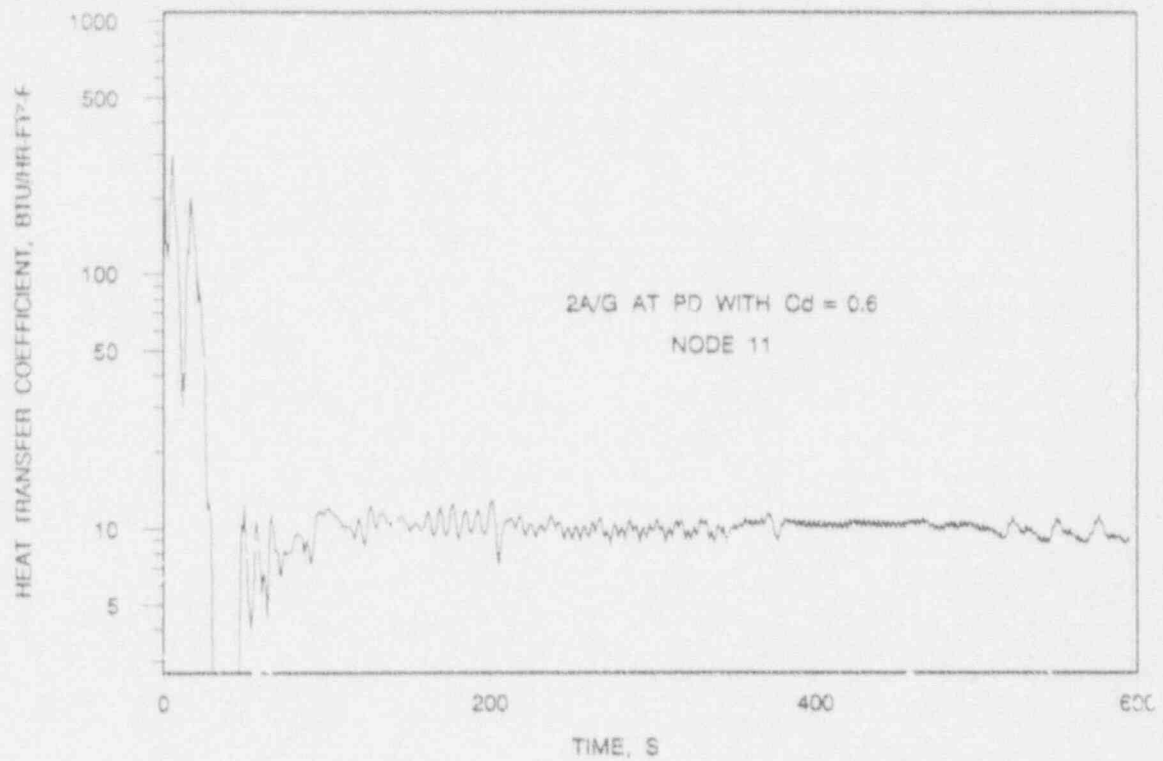


FIGURE 7-32 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.6$   
PEAK CLADDING TEMPERATURE

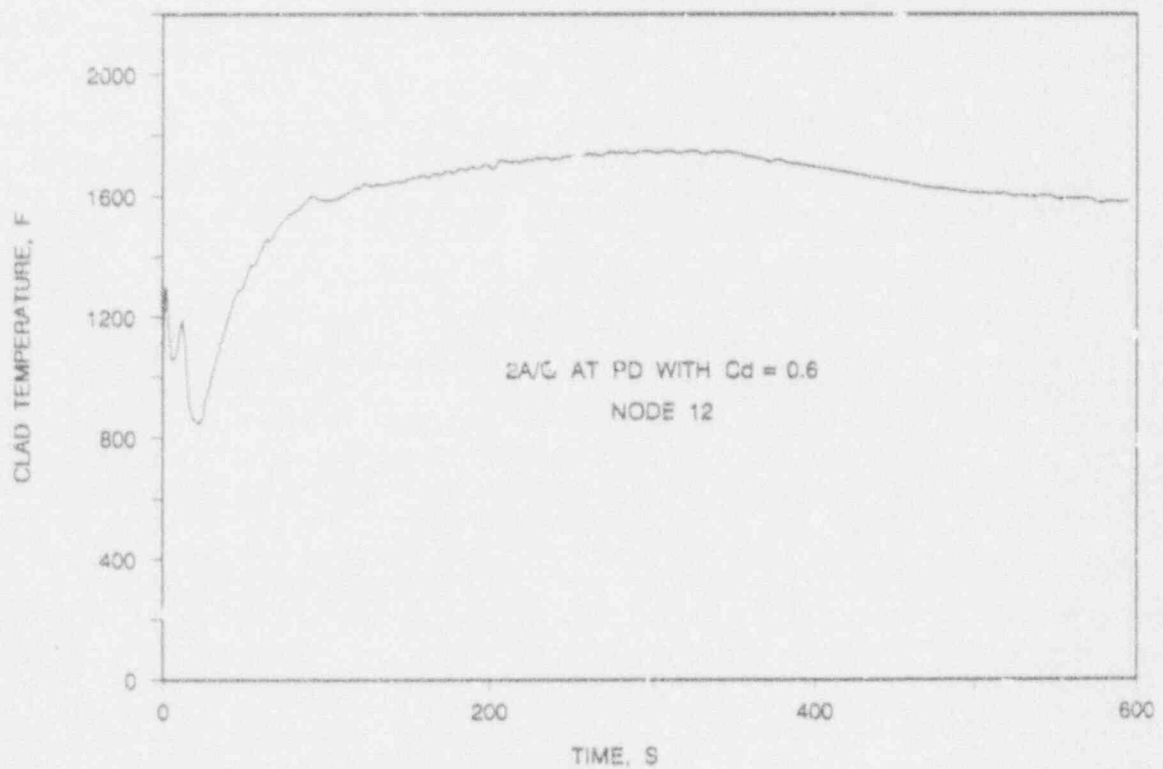


FIGURE 7-33 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.6$   
CLADDING TEMPERATURE AT RUPTURE LOCATION

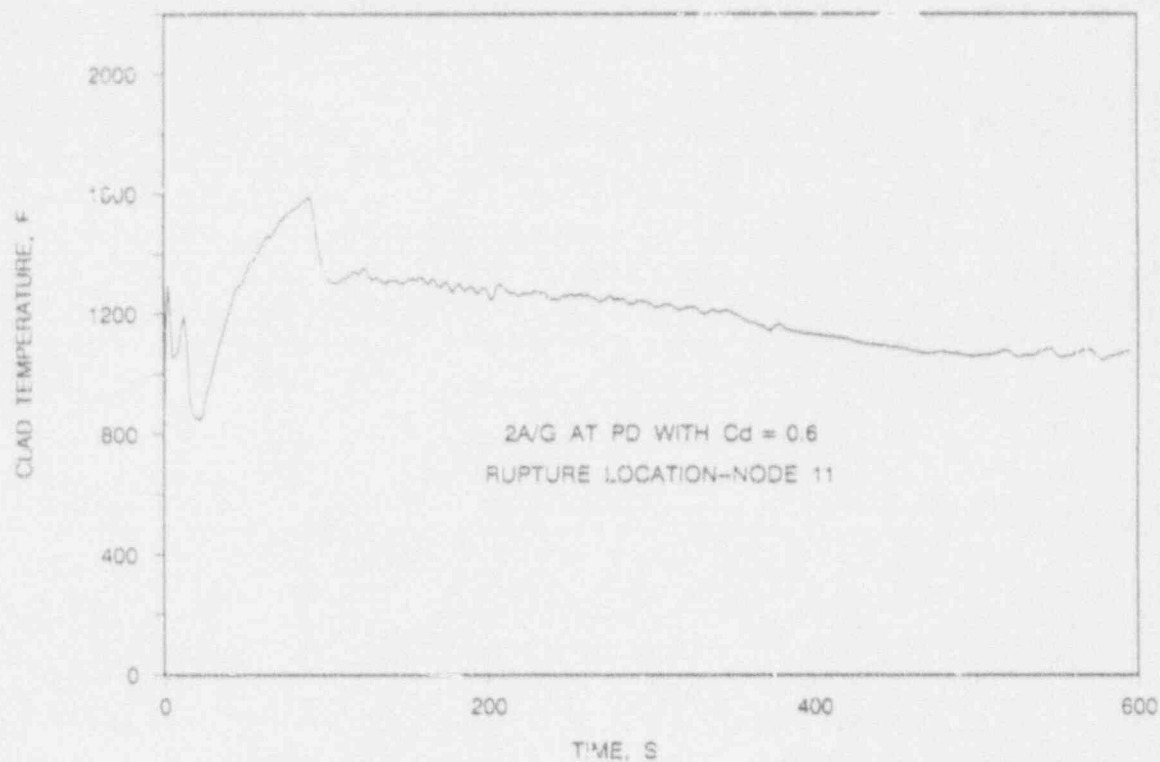


FIGURE 7-34 DISCHARGE COEFFICIENT STUDY -  $C_d = 0.6$   
CLADDING TEMPERATURE AT ADJACENT GRID SPAN

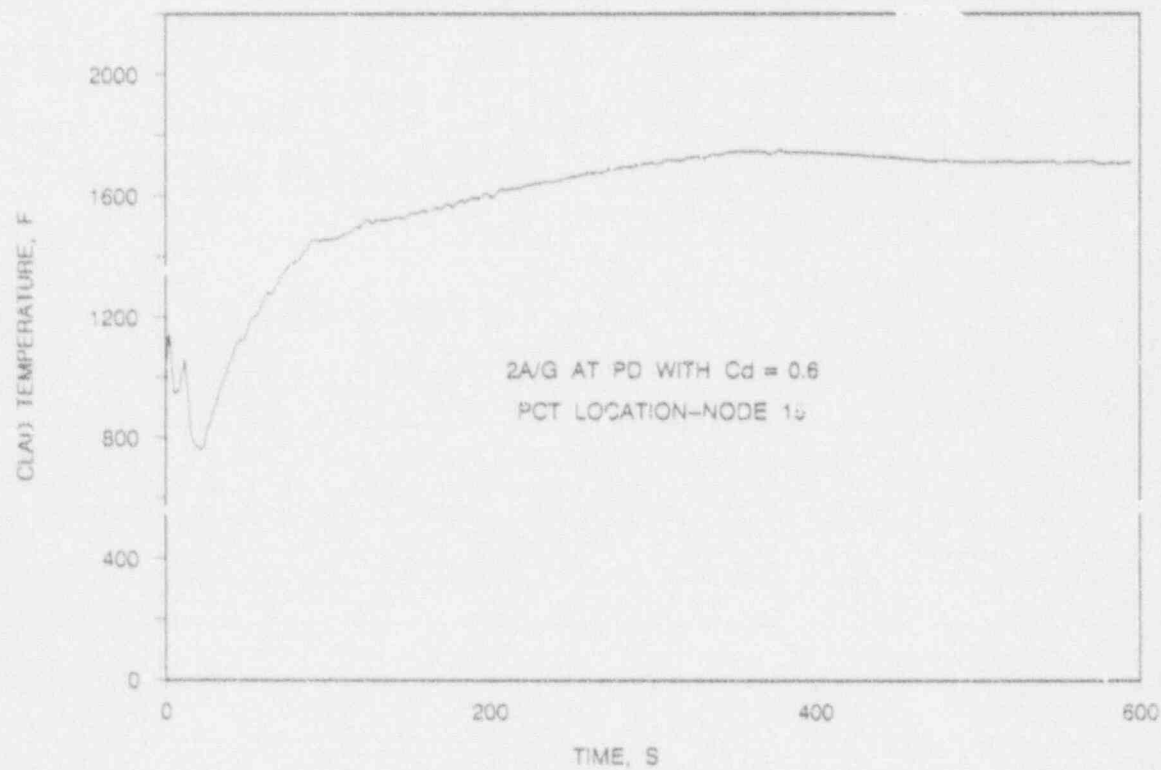


FIGURE 7-35a DISCHARGE COEFFICIENT STUDY -  $C_d = 0.6$   
 FLUID TEMPERATURE AT PCT LOCATION

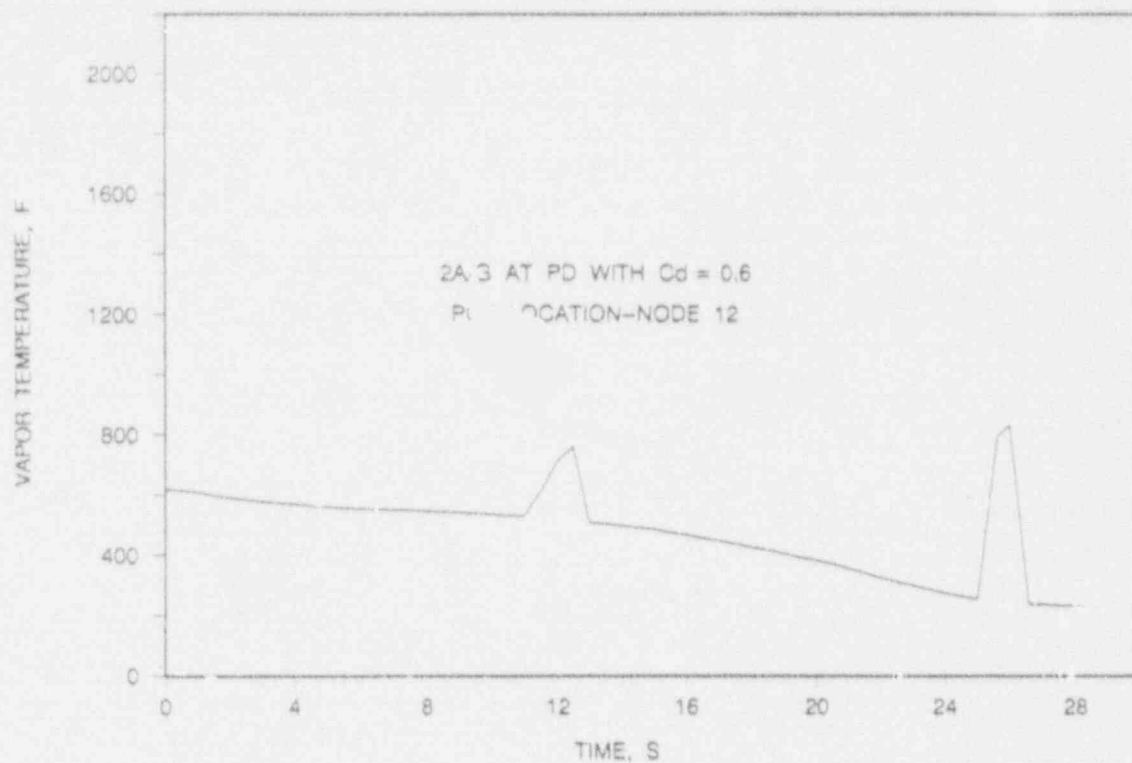


FIGURE 7-35b DISCHARGE COEFFICIENT STUDY -  $C_d = 0.6$   
 FLUID TEMPERATURE AT PCT LOCATION

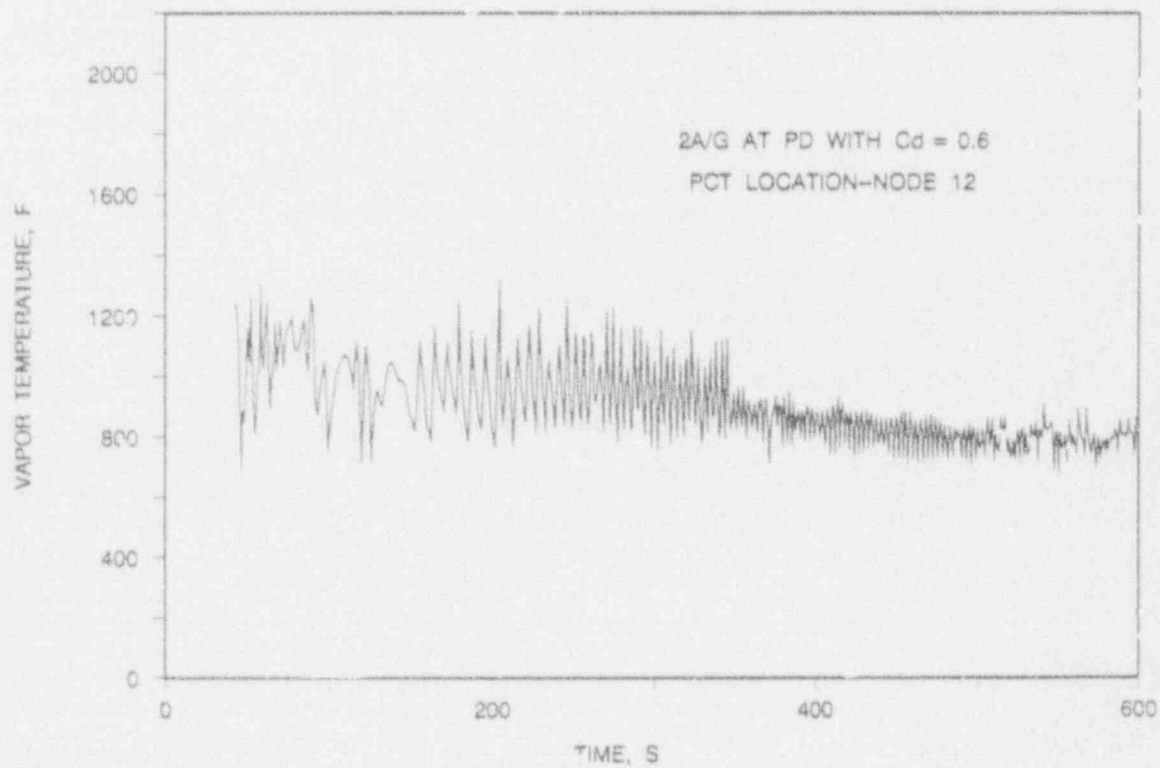


FIGURE 7-36 BREAK TYPE STUDY - SPLIT,  $C_d = 1.0$   
SYSTEM PRESSURE DURING BLOWDOWN

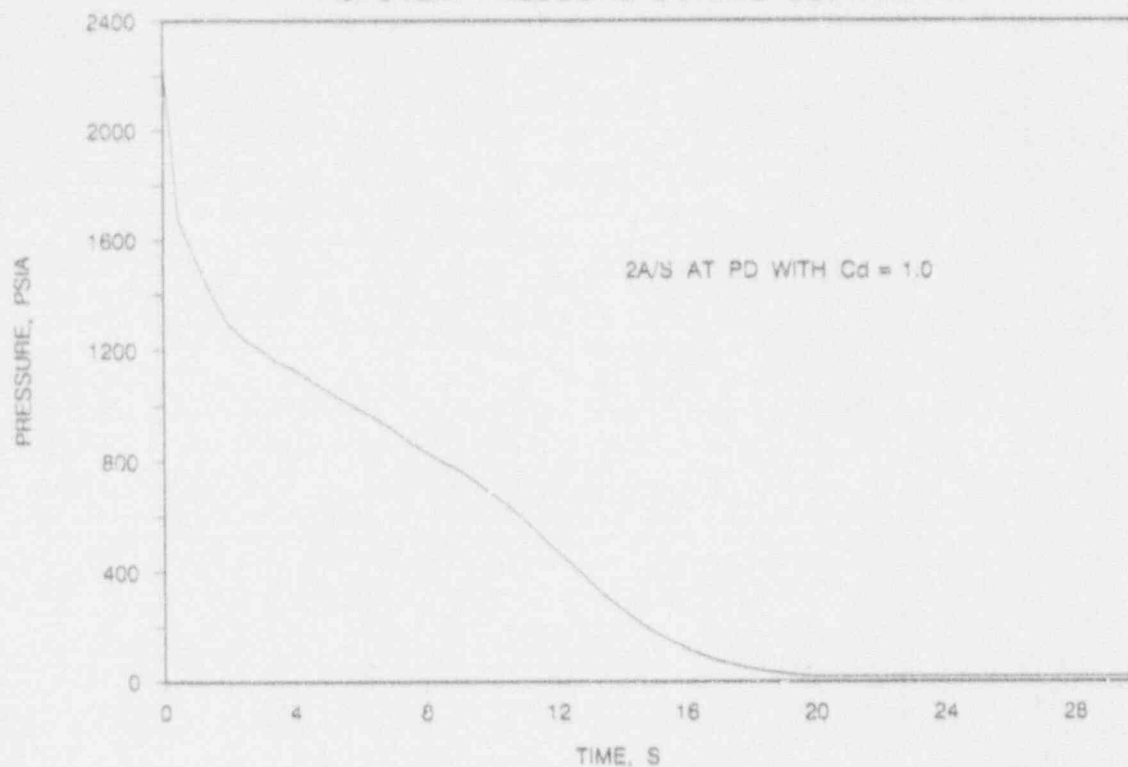


FIGURE 7-37 BREAK TYPE STUDY - SPLIT,  $C_d = 1.0$   
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

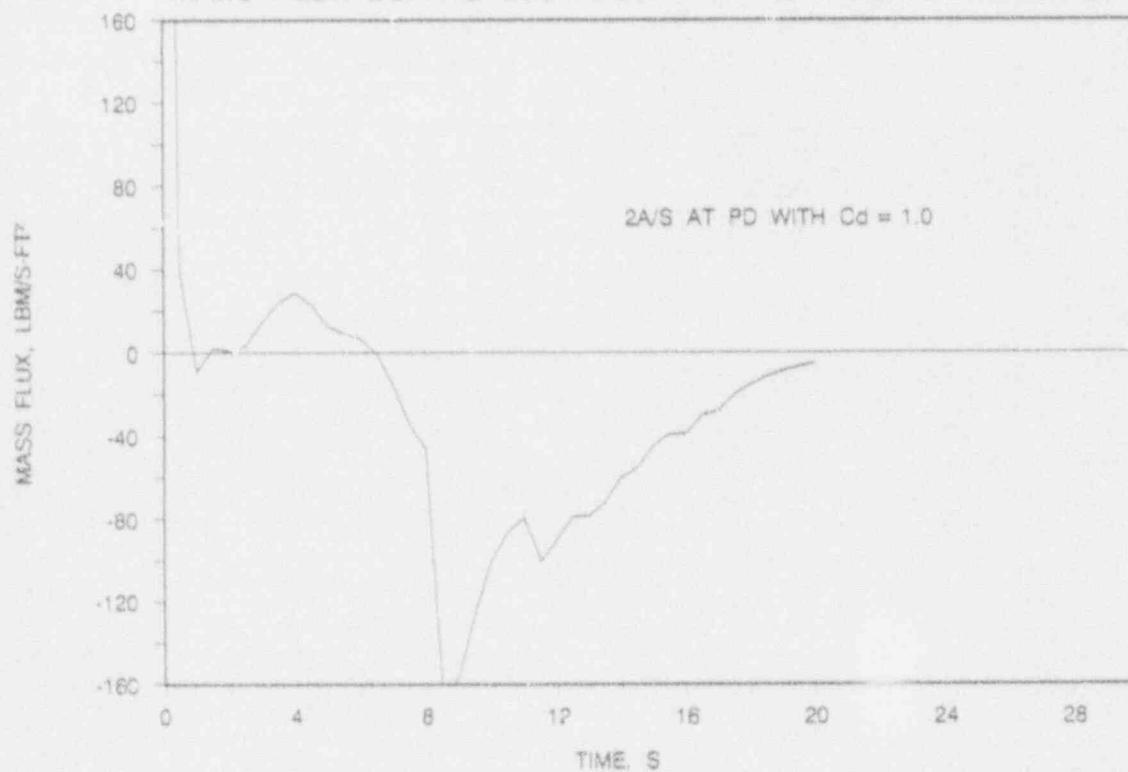


FIGURE 7-38 BREAK TYPE STUDY - SPLIT,  $C_d = 1.0$   
REFLOODING RATE

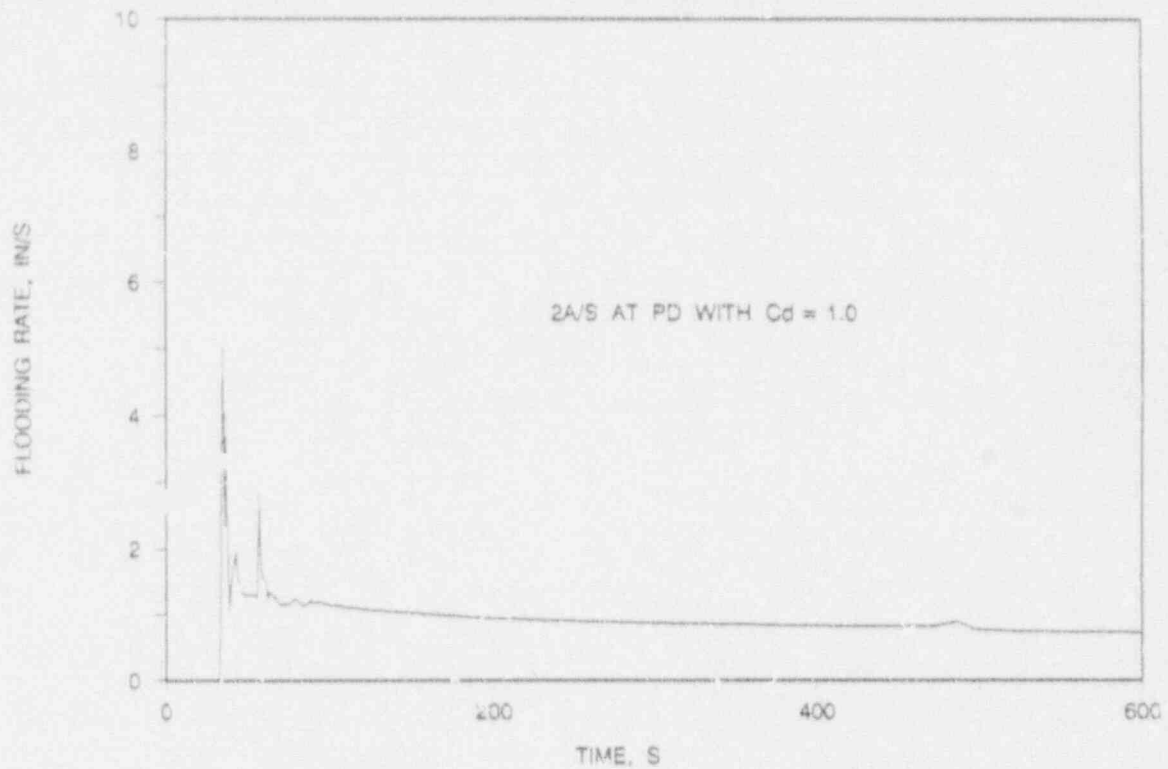


FIGURE 7-39 BREAK TYPE STUDY - SPLIT,  $C_d = 1.0$   
HEAT TRANSFER COEFFICIENT AT PEAK POWER LOCATION

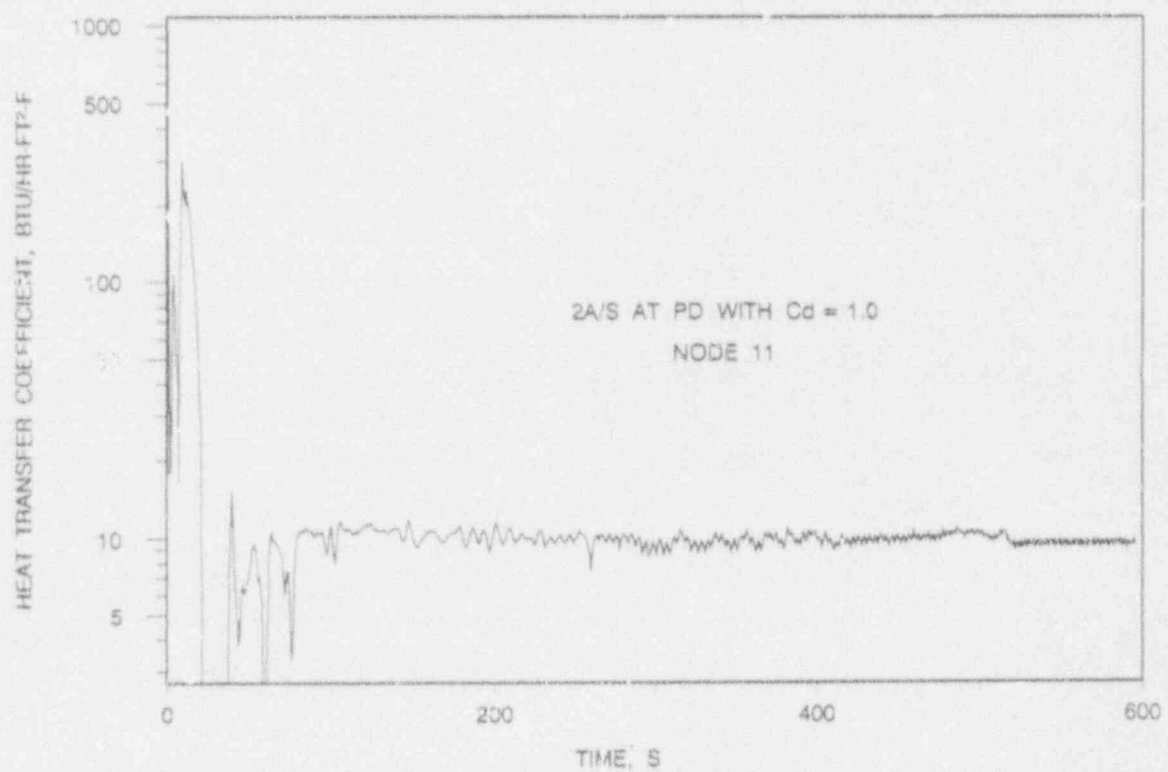


FIGURE 7-40 BREAK TYPE STUDY - SPLIT,  $C_d = 1.0$   
PEAK CLADDING TEMPERATURE

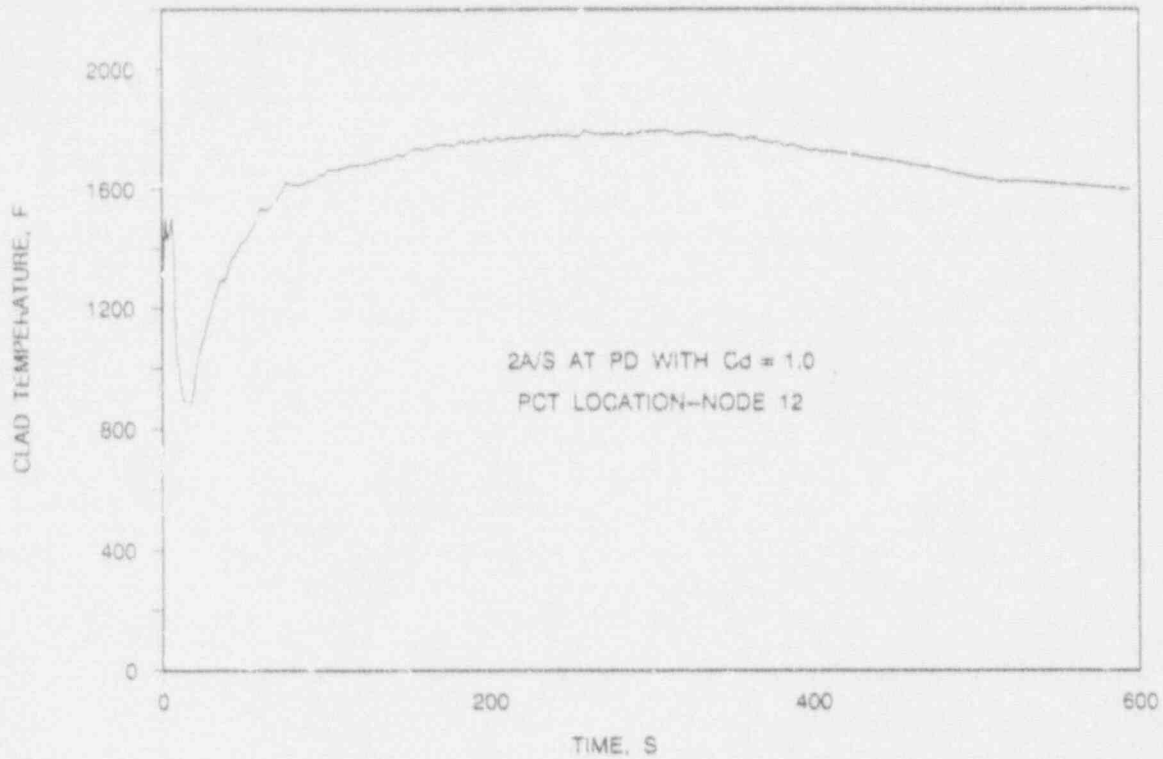


FIGURE 7-41 BREAK TYPE STUDY - SPLIT,  $C_d = 1.0$   
CLADDING TEMPERATURE AT RUPTURE LOCATION

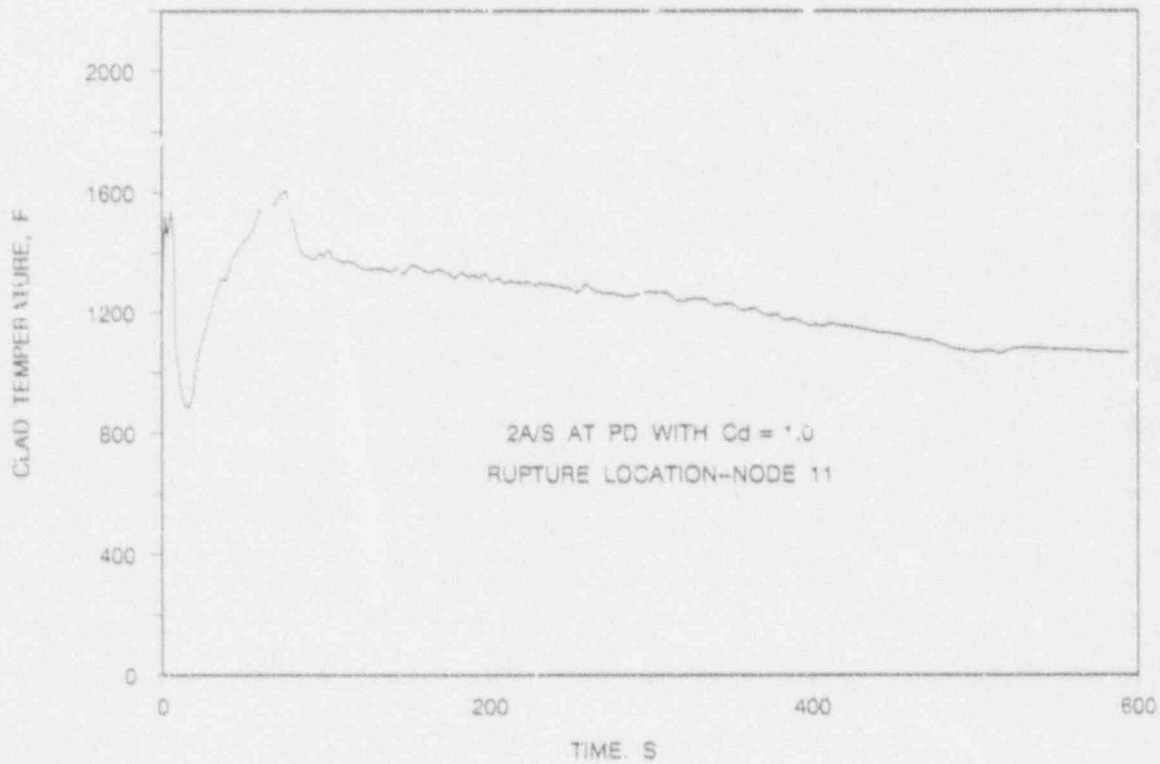




FIGURE 7-42 BREAK TYPE STUDY - SPLIT,  $C_d = 1.0$   
CLADDING TEMPERATURE IN ADJACENT GRID SPAN

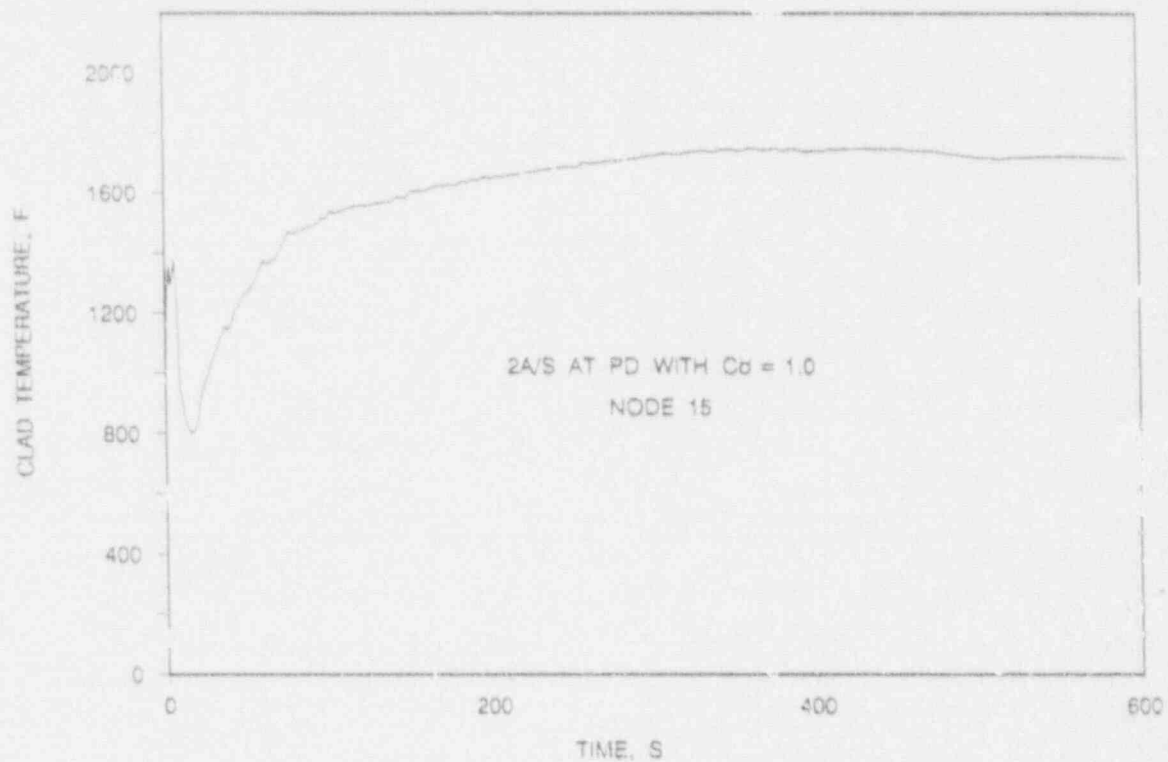


FIGURE 7-43a BREAK TYPE STUDY - SPLIT,  $C_d = 1.0$   
FLUID TEMPERATURE AT PCT LOCATION

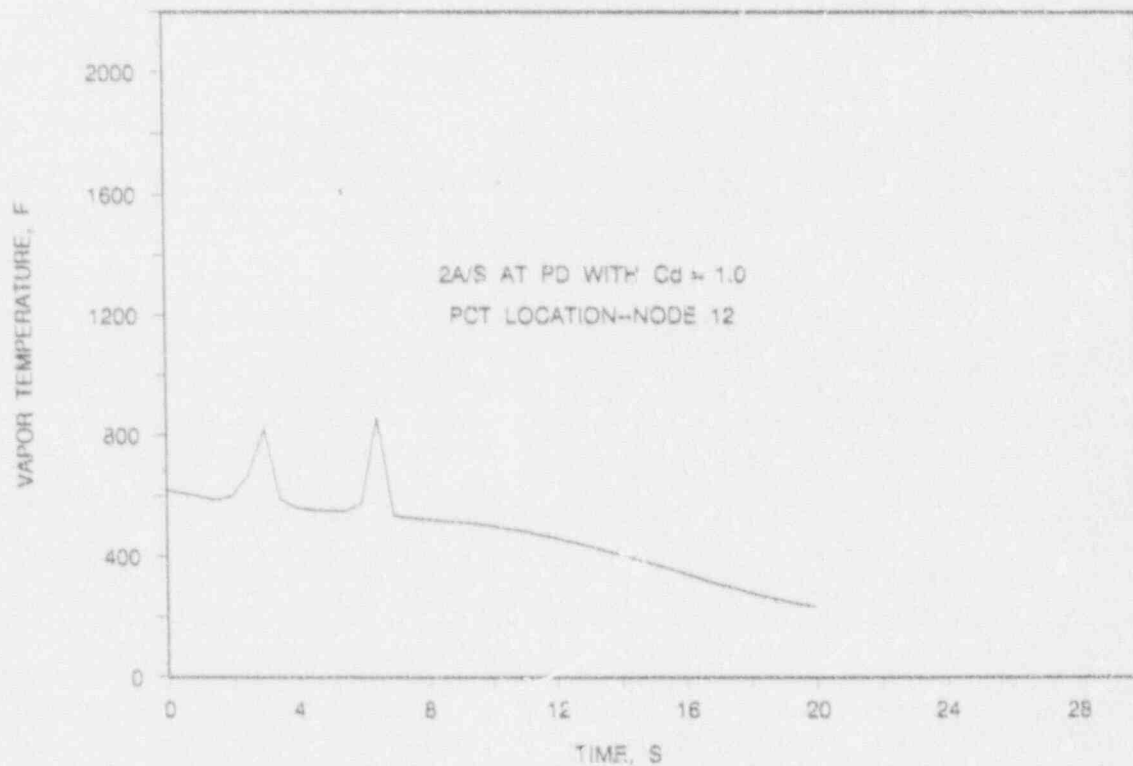


FIGURE 7-43b BREAK TYPE STUDY - SPLIT,  $C_d = 1.0$   
FLUID TEMPERATURE AT PCT LOCATION

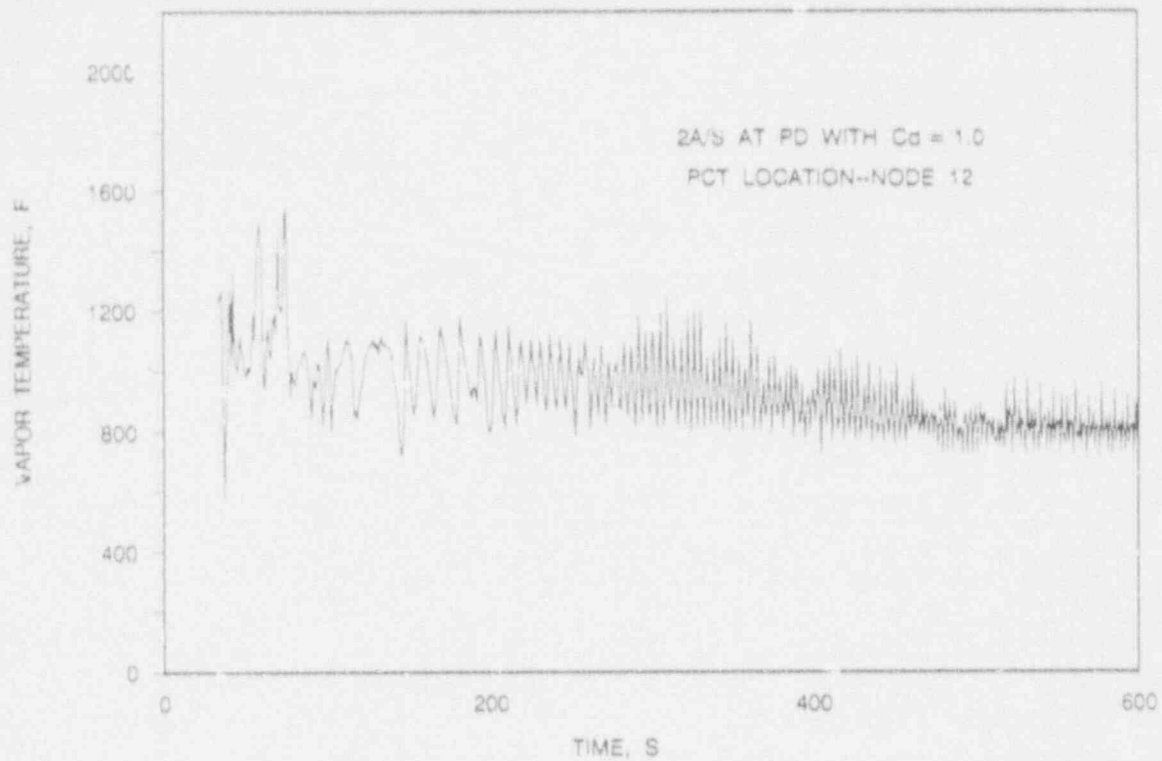


FIGURE 7-44 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
MINIMUM ECC PUMPED INJECTION CONTAINMENT PRESSURE

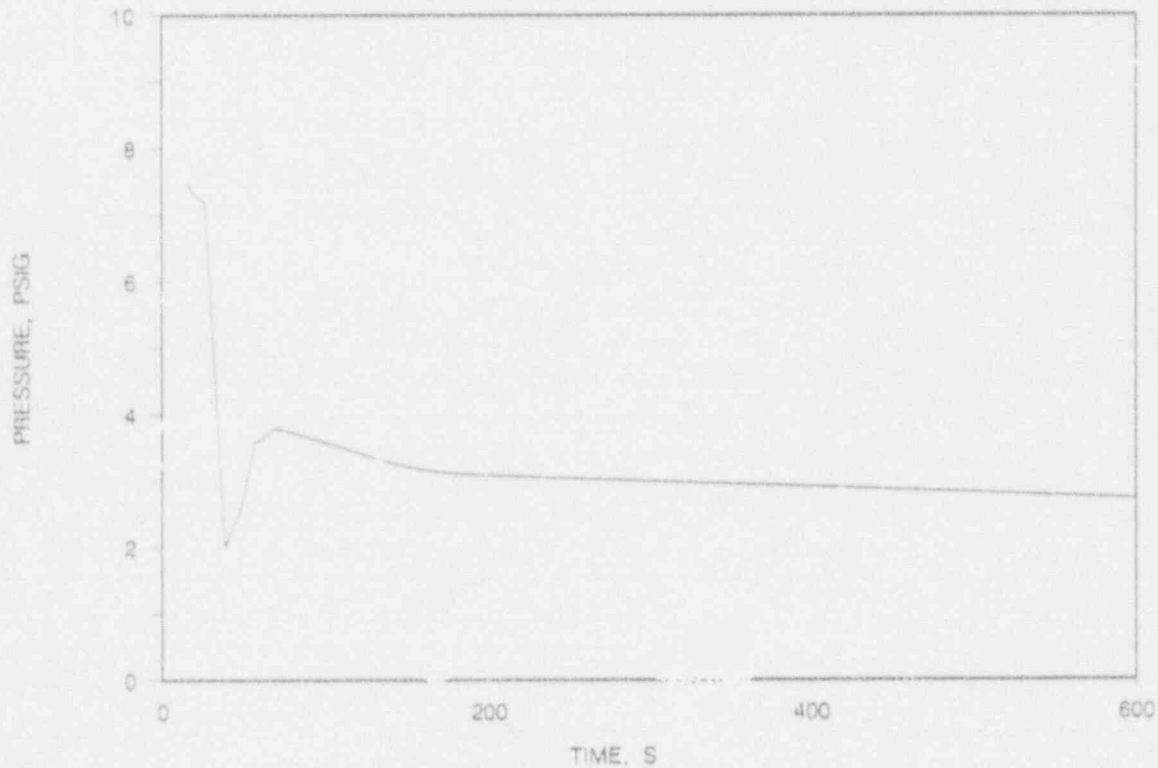


FIGURE 7-45 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
SYSTEM PRESSURE DURING BLOWDOWN

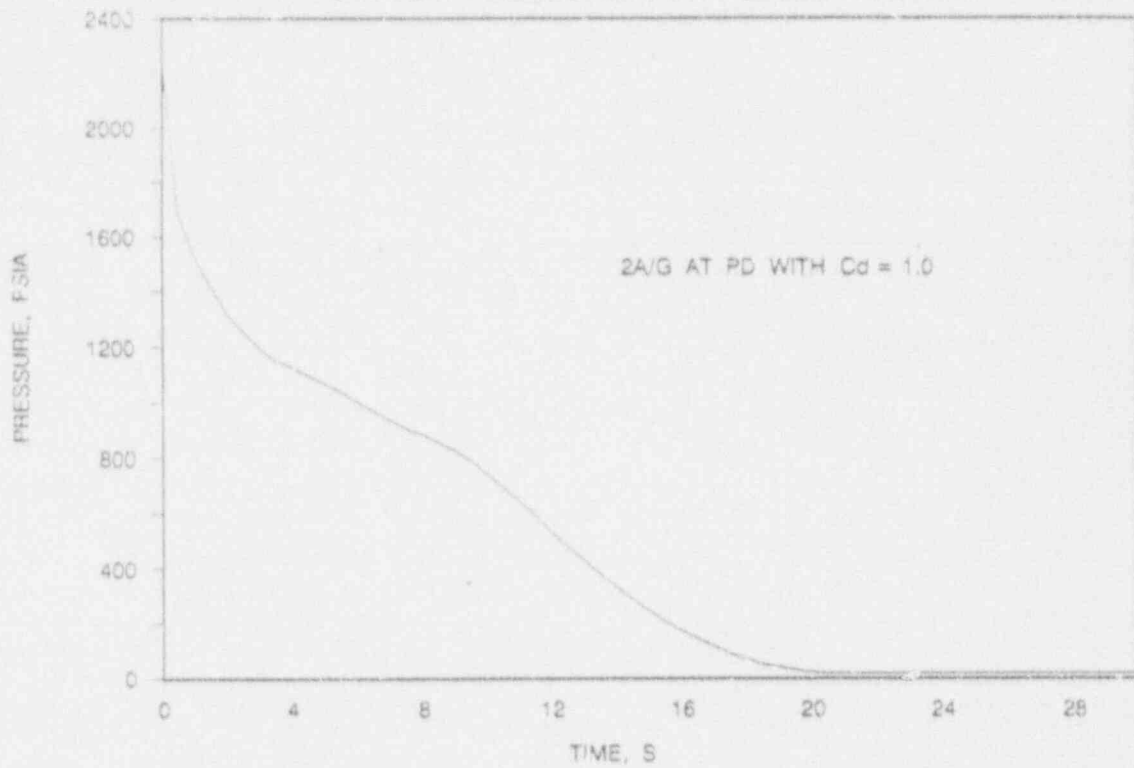


FIGURE 7-46 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

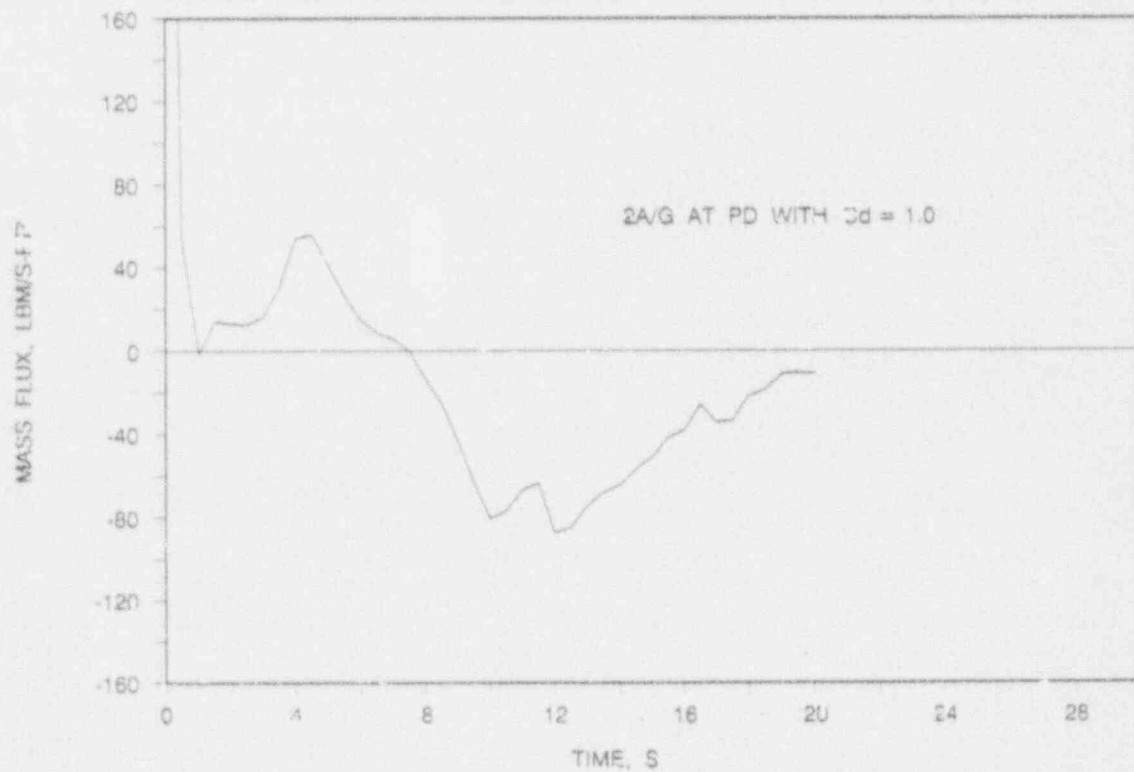


FIGURE 7-47 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
MAXIMUM ECC PUMPED INJECTION CONTAINMENT PRESSURE

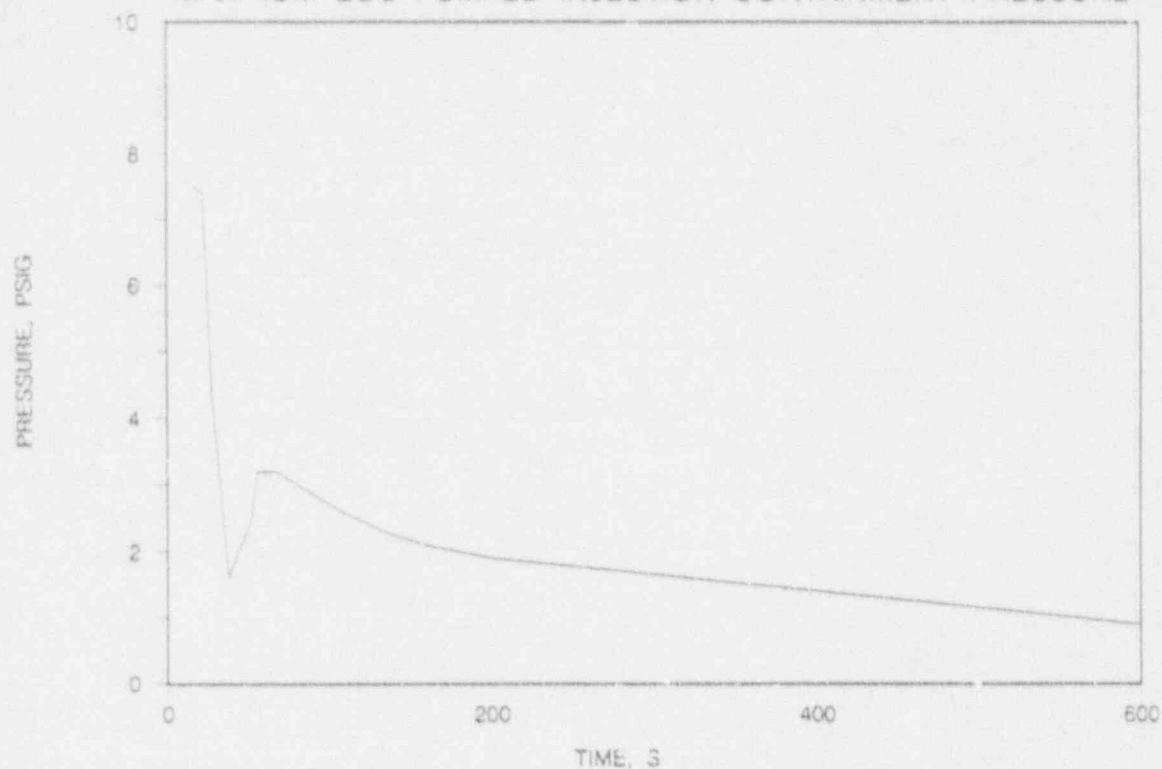


FIGURE 7-48 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
PUMPED ECC INJECTION FLOW RATE

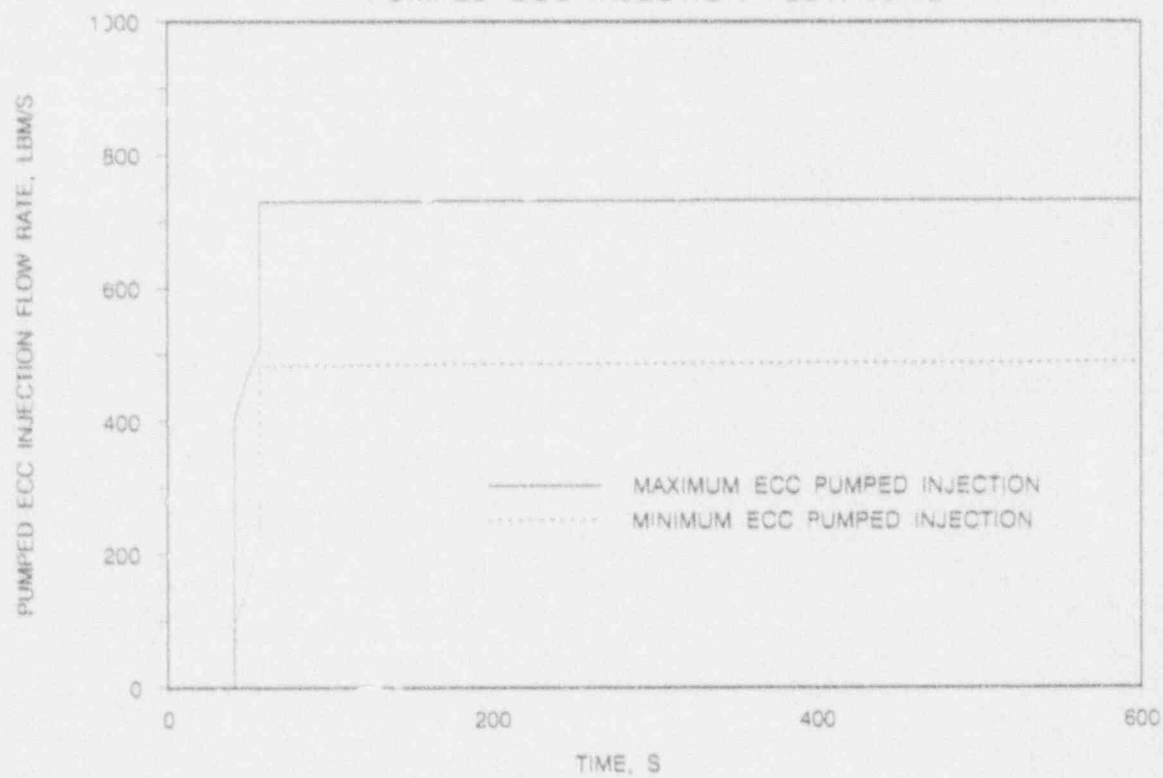


FIGURE 7-49 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
DOWNCOMER WATER LEVEL

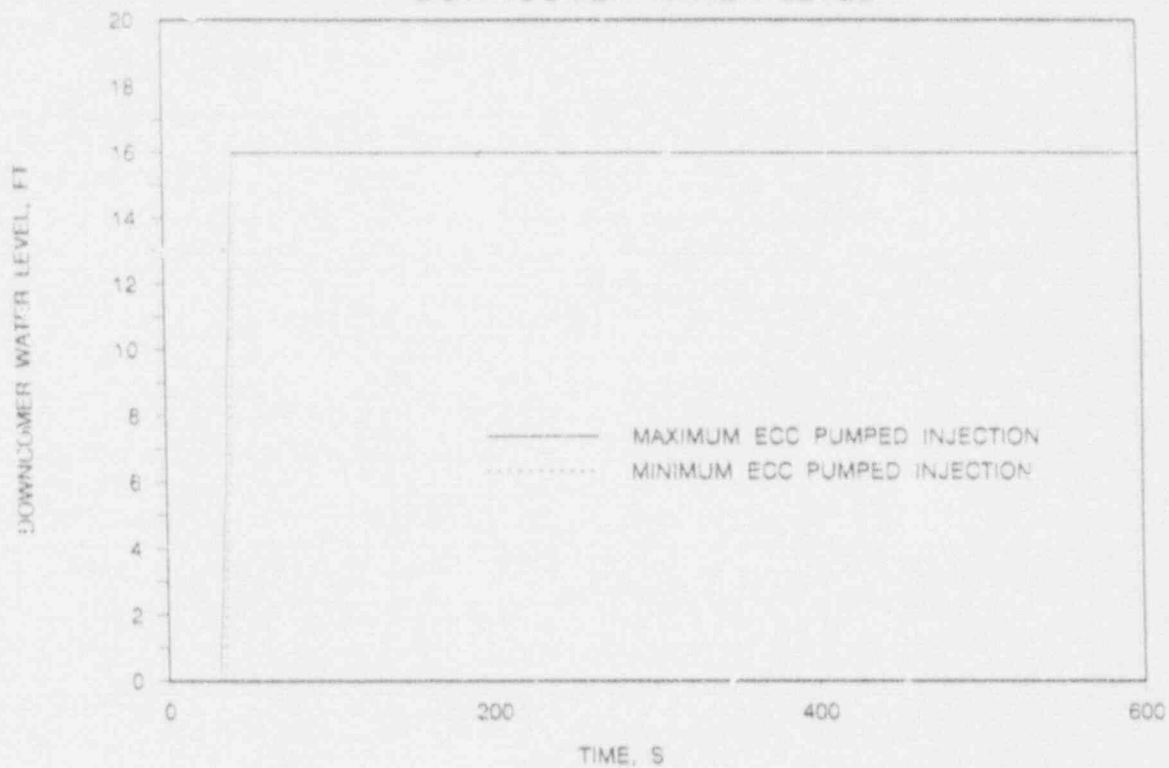


FIGURE 7-50 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
REFLOODING RATE

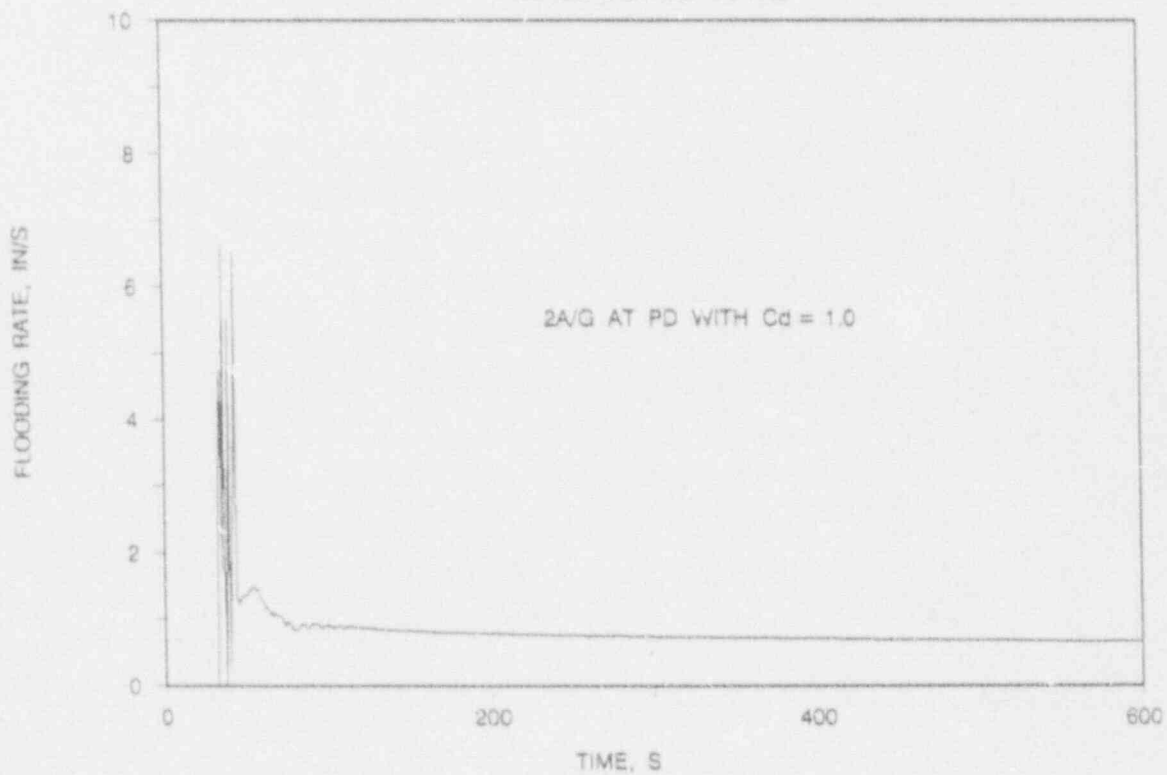


FIGURE 7-51 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
HEAT TRANSFER COEFFICIENT AT PCT LOCATION

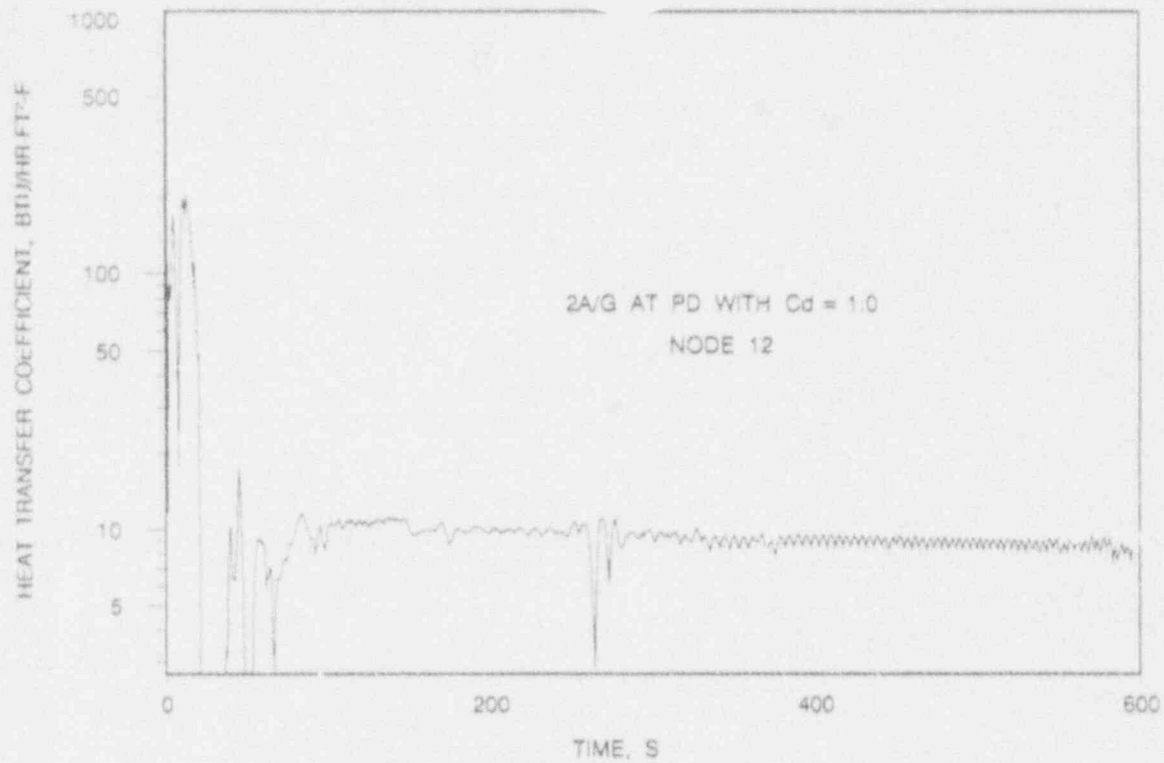


FIGURE 7-52 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
HEAT TRANSFER COEFFICIENT AT RUPTURE LOCATION

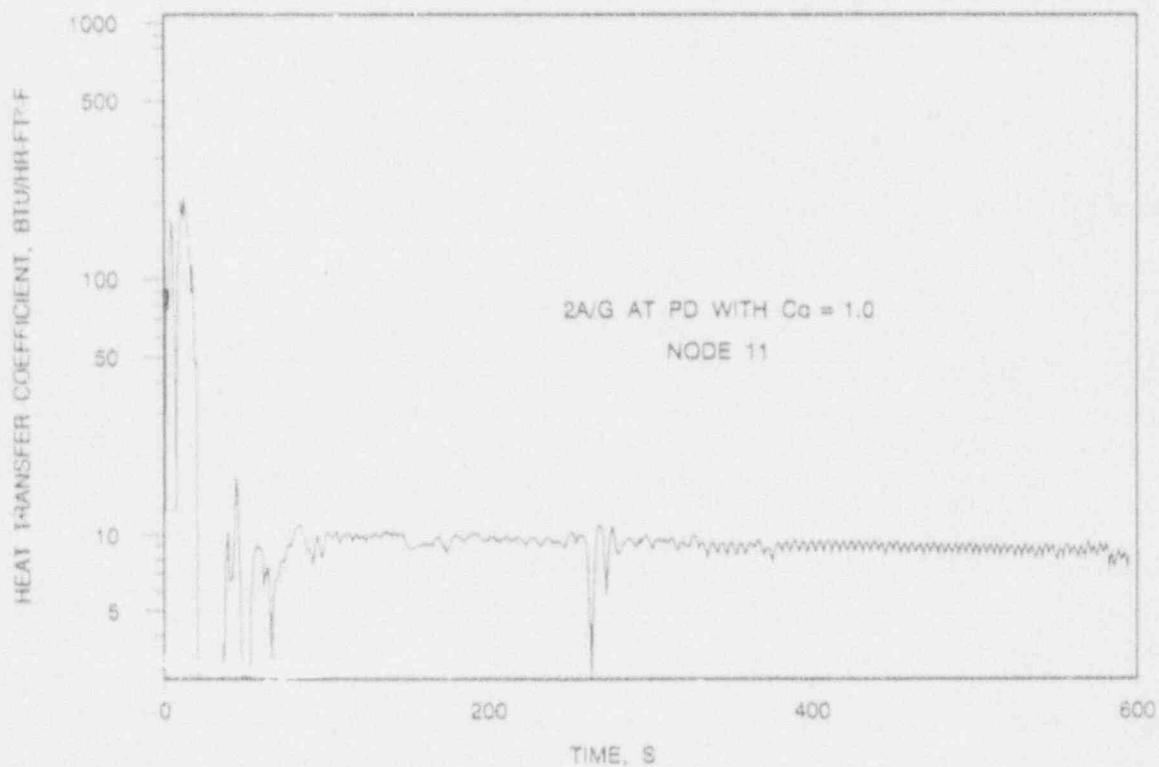




FIGURE 7-53 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
HEAT TRANSFER COEFFICIENT IN ADJACENT GRID SPAN

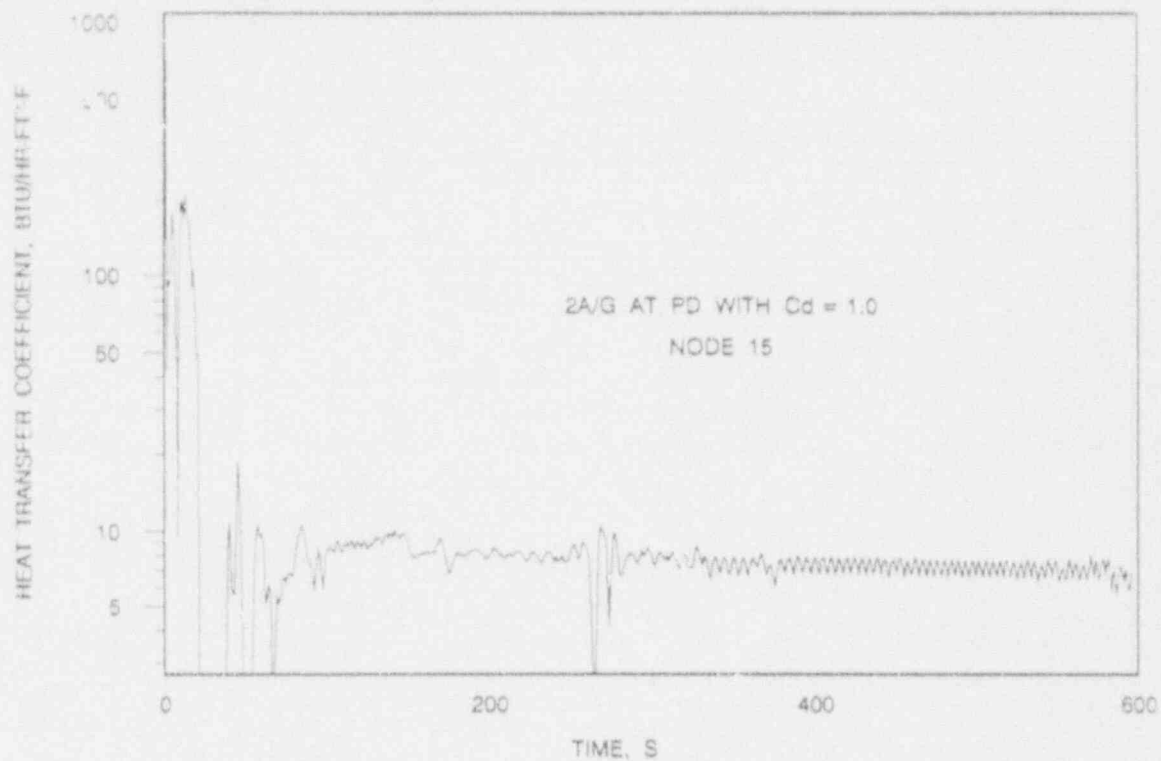


FIGURE 7-54 MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
PEAK CLADDING TEMPERATURE

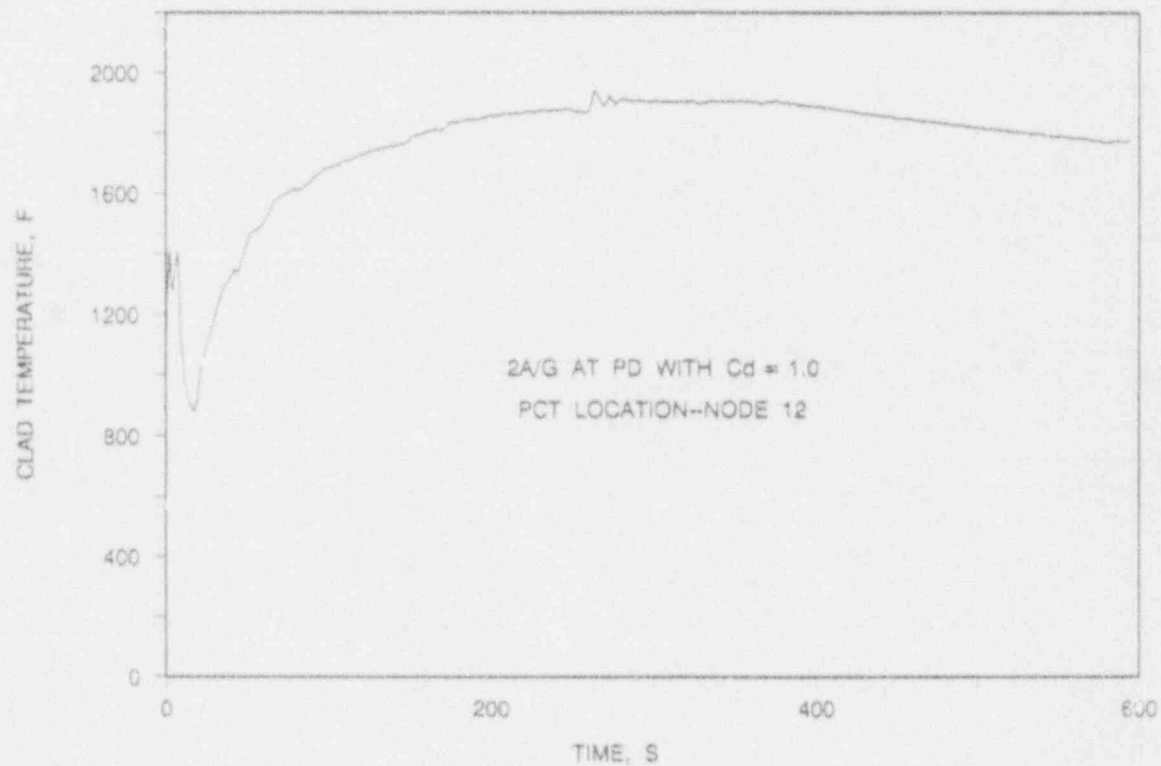


FIGURE 7-55 MAXIMUM ECCS STUDY - DECLB, Cd = 1.0  
CLADDING TEMPERATURE AT RUPTURE LOCATION

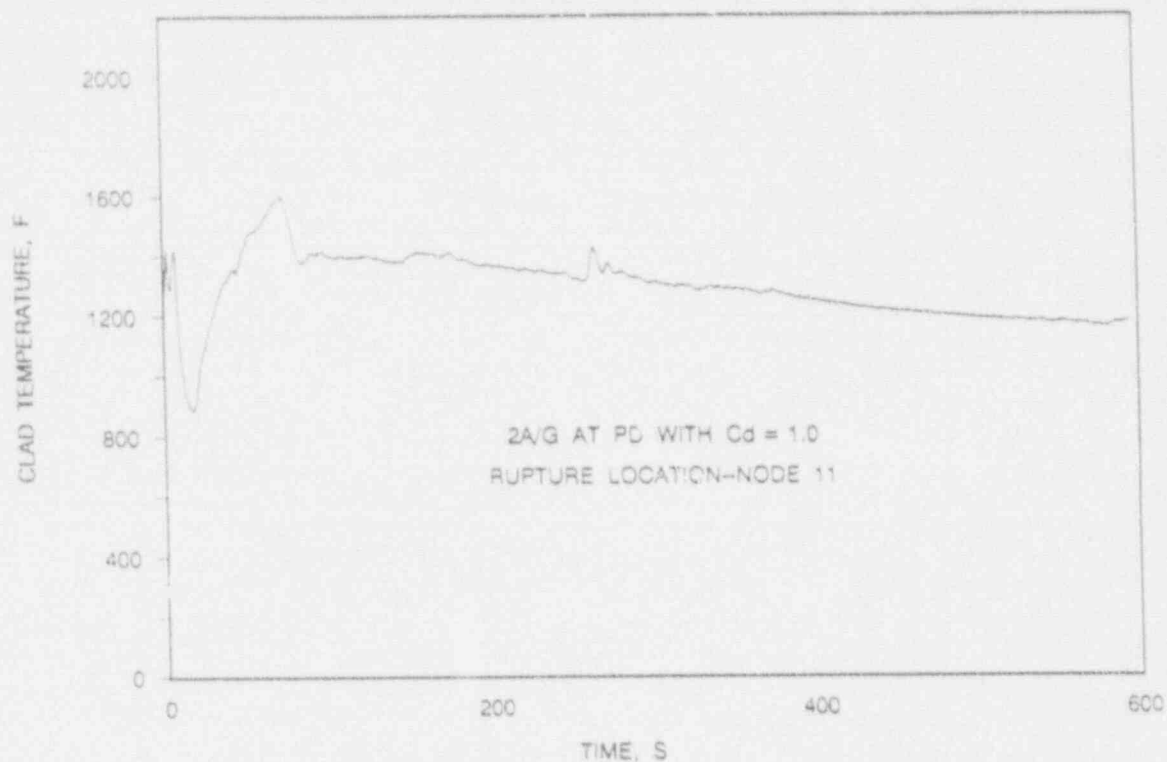


FIGURE 7-56 MAXIMUM ECCS STUDY - DECLB, Cd = 1.0  
CLADDING TEMPERATURE IN ADJACENT GRID SPAN

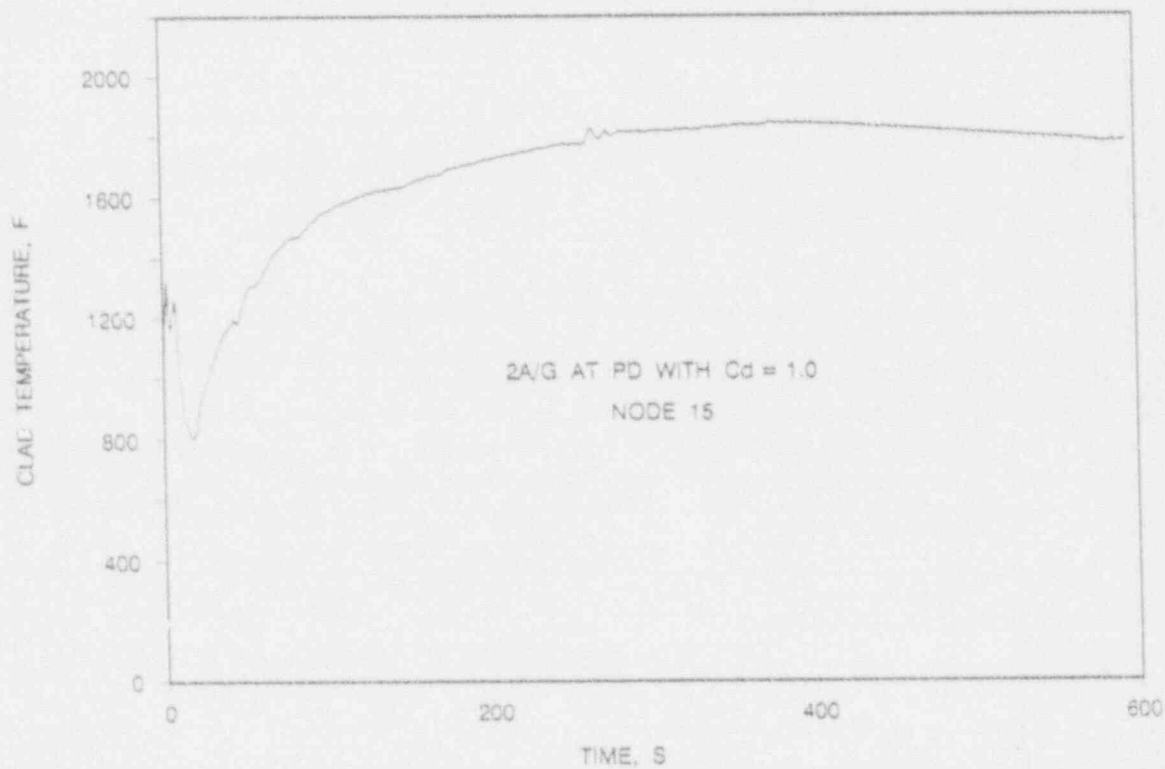


FIGURE 7-57a MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
FLUID TEMPERATURE AT PCT LOCATION

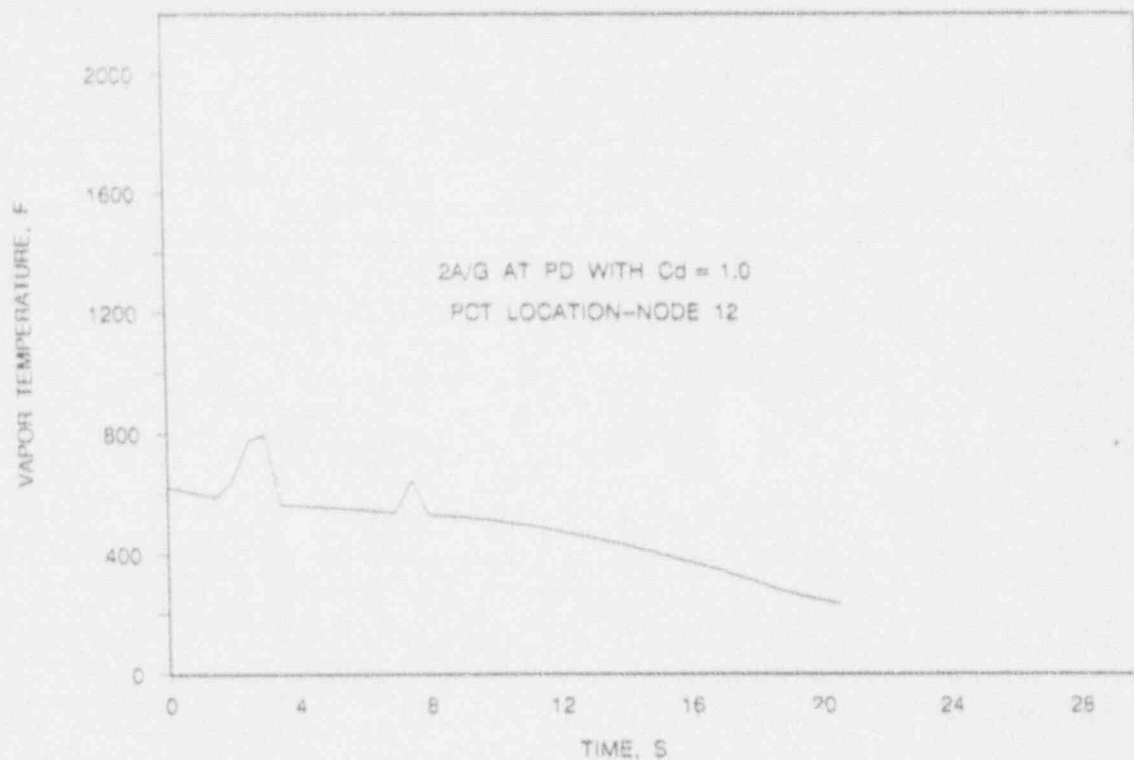


FIGURE 7-57b MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
FLUID TEMPERATURE AT PCT LOCATION

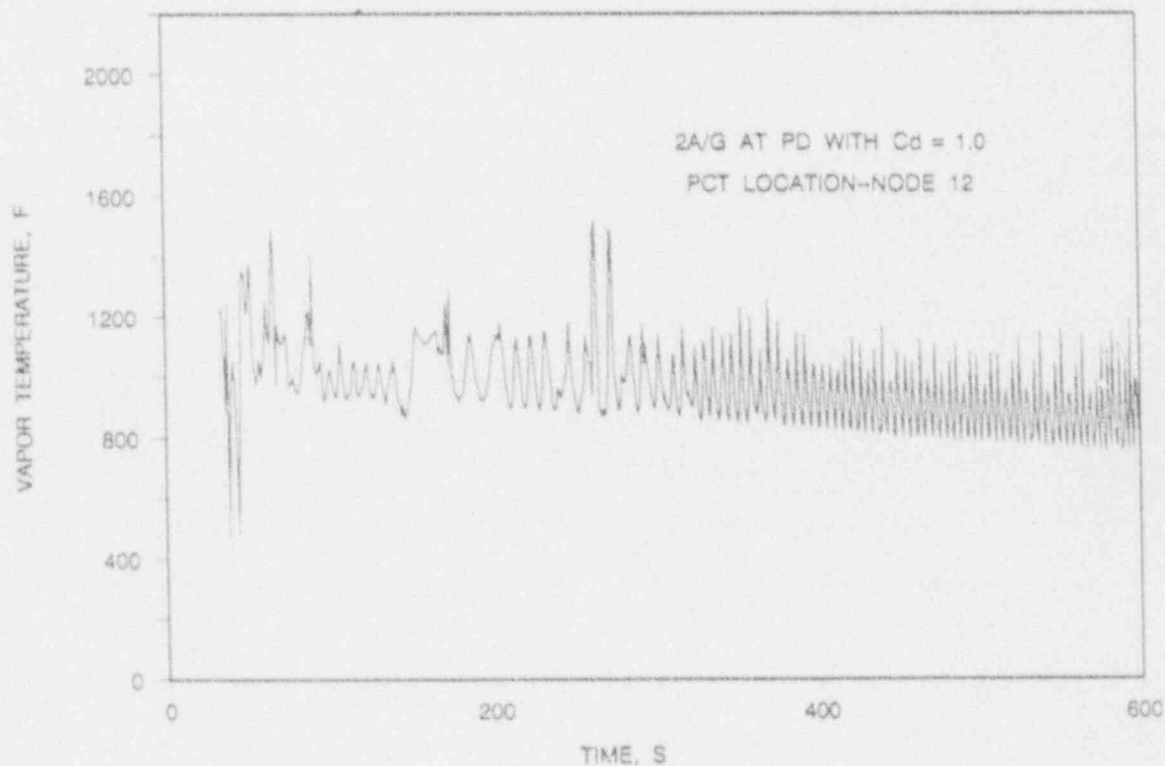


FIGURE 7-58a MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
FLUID TEMPERATURE AT RUPTURE LOCATION

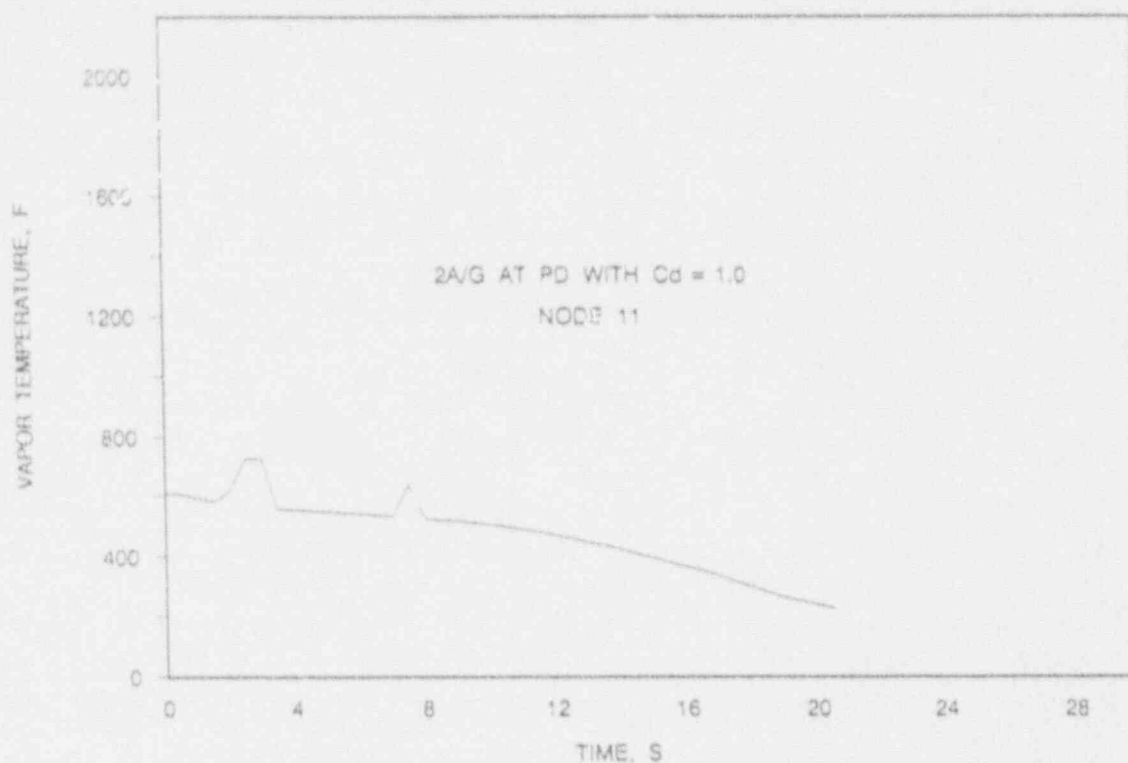


FIGURE 7-58b MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
FLUID TEMPERATURE AT RUPTURE LOCATION

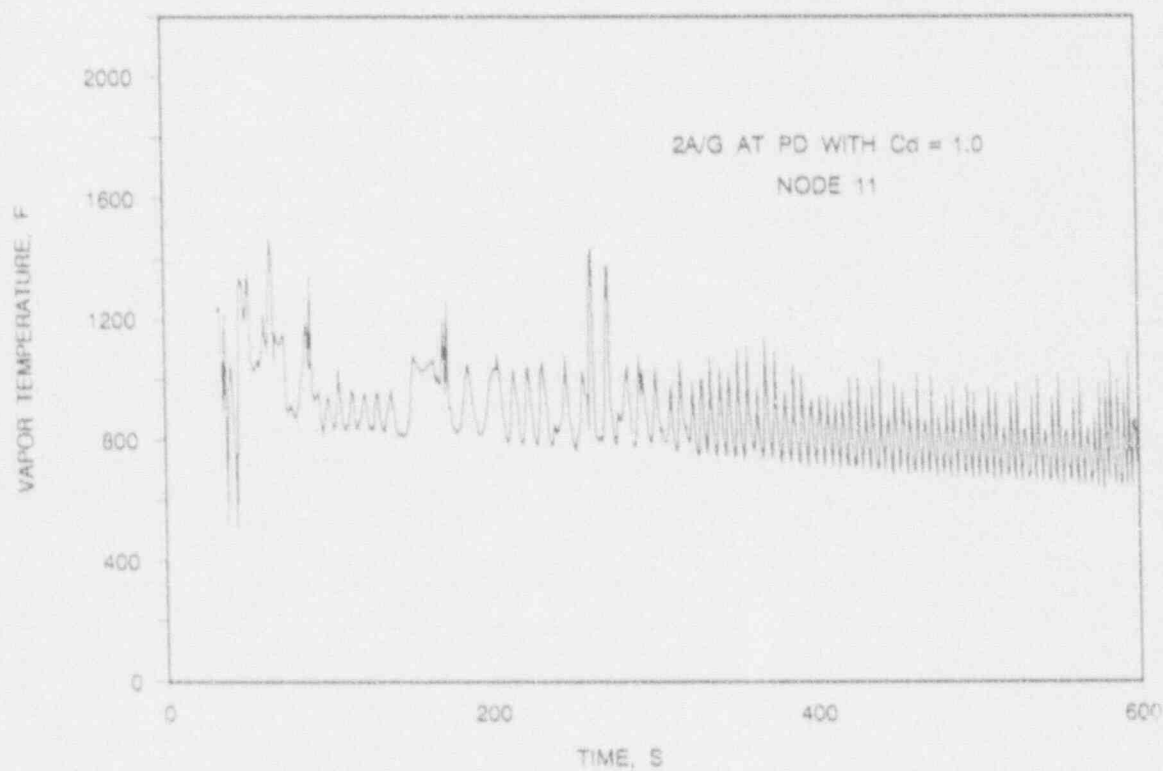


FIGURE 7-59a MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
FLUID TEMPERATURE IN ADJACENT GRID SPAN

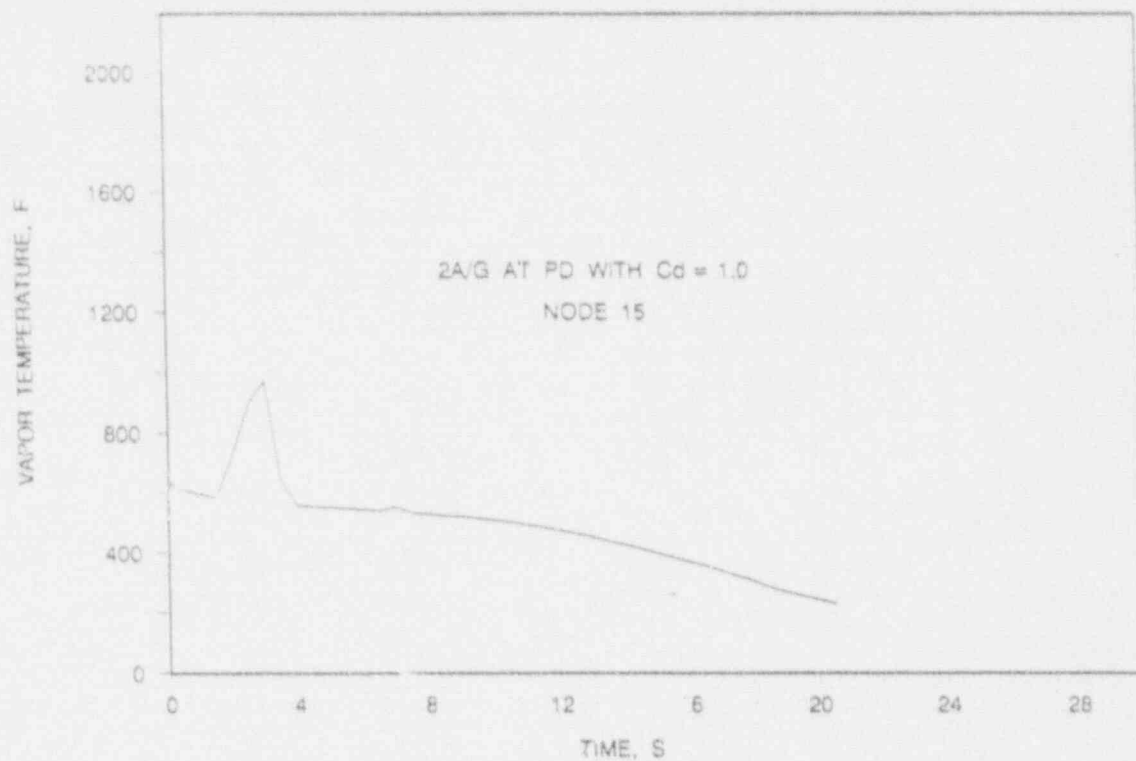
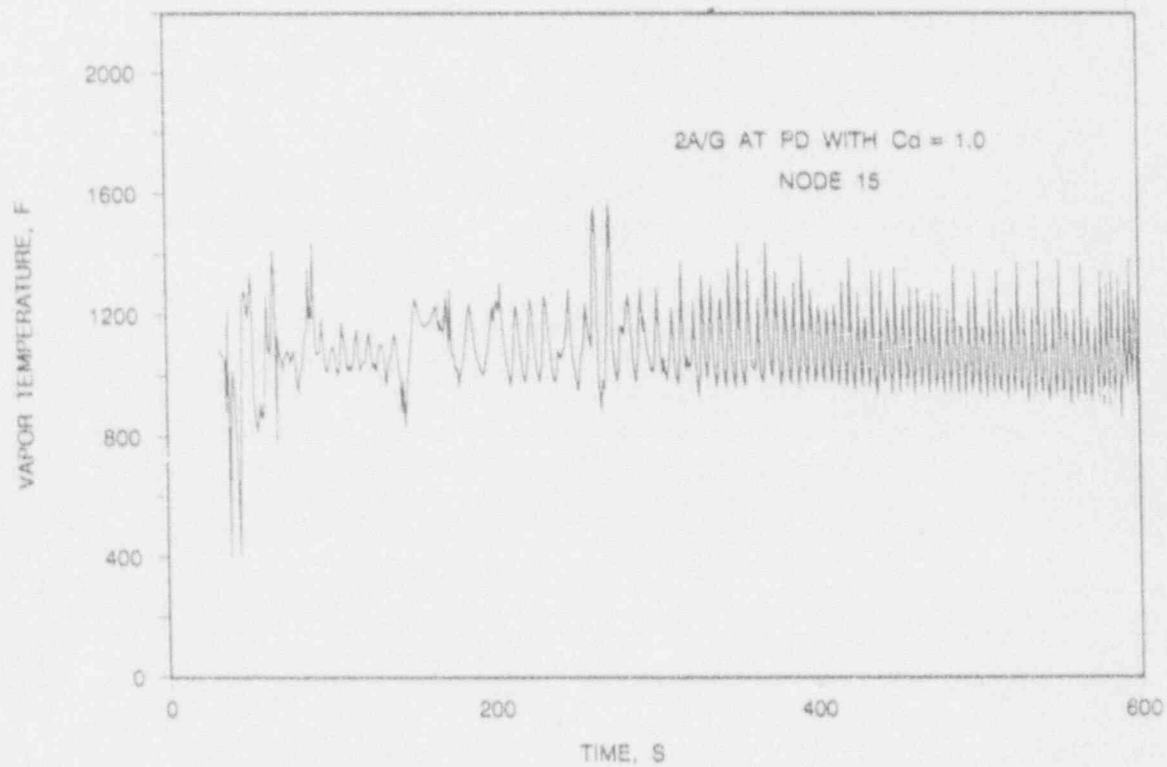


FIGURE 7-59b MAXIMUM ECCS STUDY - DECLB,  $C_d = 1.0$   
FLUID TEMPERATURE IN ADJACENT GRID SPAN





## B. LOCA Limits

The LOCA evaluation is completed with a set of analyses done to show compliance with 10 CFR 50.46 for the core power and peaking that will be taken as the limiting LOCA conditions for core operation, that is, the LOCA limits. The term limit is applied because these cases are run at the limit of allowable local power operation. Actually, these LOCA evaluations serve as the bases for the allowable local power. As such, the LOCA limits calculations comprise the cases that are used to demonstrate compliance of the reload fuel cycles and peaking limits to the criteria of 10 CFR 50.46. Five runs are made at differing axial elevations such that a curve of allowable peak linear heat rates as a function of elevation in the core can be constructed or, in this case, confirmed. This curve becomes a part of the plant technical specifications, and plant operation is controlled such that the local peaking and power do not exceed the allowable values. (Note: The 9.7' LOCA limits case has been reanalyzed at a higher  $F_0$  and the results of the case are reported in Appendix B along with a revised total peaking factor curve, Figure B-1.)

### B.1 LOCA Limits Conditions

The absolute LOCA limits to power and peaking for each elevation in the core can be determined through repeated calculations at each elevation, with successively higher local power levels, until the analysis shows one or more of the applicable acceptance criteria to be exceeded. The highest linear heat rate for which the criteria are not exceeded is the absolute LOCA limit for a particular elevation. The more practical approach, the one adopted for this report, assumes a set of peaking limits at a given power level that have been determined to be acceptable for fuel cycle design and plant operations purposes. The LOCA limits



analyses are then done to confirm that the assumed limits will meet the applicable criteria.

Figure 8-1 shows the axial power and peaking selected and confirmed as applicable to the McGuire and Catawba plants for operation with Mark-BW fuel. With the axial power and peaking dependency established, LOCA calculations are performed with the core power level and total peaking initialized at different positions on the curve to demonstrate that these peaking limitations assure compliance with 10 CFR 50.46. Should the results not comply, the allowed peaking is reduced, and the analysis is repeated until acceptable results can be obtained. Likewise, if the results show large margins of compliance, the peaking may be increased to provide additional operational flexibility. For these analyses, neither of these steps was taken although the results do show considerable margins at certain elevations.

An additional condition assumed in these analyses is that the allowable peaking will be dependent on fuel assembly burnup in accordance with Figure 8-2. This limitation is made necessary because, at burnups approaching 50000 MWd/MTu, the initial fuel enthalpy and internal pressure can become a more severe combination than the beginning-of-life values. By assuring that the local heating rates will be limited to those shown in Figure 8-2, the reduction in power compensates for the increases in fuel temperature and pin pressure such that the beginning-of-life conditions remain the most severe. (This is discussed in greater detail in the time-in-life sensitivity studies, Section 6.2.) Therefore, Figure 8-2 is a limit of operation for the Mark-BW fuel. The limit is checked during the fuel design process. However, at the high burnup at which the limit is imposed there should be no restrictions on core operation, because the highly

depleted fuel is unlikely to reach the limit within the operational envelope of the plant Technical Specifications.

## 8.2 LOCA Limits Results

To validate Figure 8-1, five separate LOCA calculations were performed. Power peaks were run centered at the middle of the second through the sixth grid spans. Figure 8-3 shows the axial power shapes evaluated. For all cases, the radial power peaking was 1.55. The combination of the axial peaking of Figure 8-3 and a 1.55 radial yields the total peaking at the corresponding elevation shown in Figure 8-1.

The results of the calculations are tabulated in Table 8-1 and shown in Figures 8-4 through 8-23. The figures comprise five sets with four figures in each set. The four figures of each set show (1) the mass flux at the elevation of peak power, (2) the cladding temperature for three different locations on the pin, (3) the heat transfer coefficient at the location of highest cladding temperature, and (4) the distribution of cladding oxidation along the pin. Only one mass flux plot is provided for each case because the axial variations in mass flux are not strong. This can be observed by comparing the five mass flux curves for the different peaking cases.

To demonstrate the cladding temperature results, three curves are presented for each case. Temperature histories are shown for the rupture location, for the node adjacent to the rupture, and for the high temperature node in an adjacent grid span. For power distributions peaked toward the middle of the core, rupture location is almost certain to correspond to the location of peak power. Near the time of rupture, the portion of the pin immediately above the rupture site will be at nearly the same temperature. Following rupture, the burst location cools quickly

as the cladding pulls away from the fuel, and the area for heat transfer is increased. Due to axial heat conduction in the cladding and the effect of the rupture on flow conditions, the cooling in the node just above the rupture is substantially improved. This means that although one of the nodes in the adjacent grid span is at a lower power, it can develop as the location of the highest cladding temperature.

The heat transfer coefficient (HTC) is shown for the peak cladding temperature location. HTC variations with elevation are as expected (see Figures 7-51 through 7-53), such that the HTC from one elevation reasonably characterizes the other elevations. The last figure in each set shows the local oxide thickness as a function of elevation for the fuel pin. Each figure shows total oxidation including that assumed prior to the start of the accident. Oxidation up to the time the cladding falls below 1500 F or the elevation has been covered by mixture, as measured by the REFLOD3B core water level, is included. The large variations of the resultant curve reflect the relatively lower cladding oxidation in the vicinity of the grid and rupture locations.

#### 2.9-ft Peak Power Case

In this case, the axial power shape is peaked well below the core midplane, and the cladding temperature responses differ accordingly from those calculated in the 4-, 6-, and 8-ft cases. The peak power locations on the rod are cooled rapidly during reflood and have not reached temperatures sufficient to cause a rupture by the time of temperature turnaround. Therefore, the rupture occurs in node 8, the center node of the grid span above the location of peak power. This region of the core is also cooled rapidly, and the peak cladding temperature occurs in the grid span above the ruptured location. Although the power at the midplane is about 80 percent of that at the peak power location,

the central node in the mid-core grid span produces the highest cladding temperature, 1816 F. The highest local oxidation, 3.4 percent, occurs at the ruptured location. The whole core oxidation calculated for this LOCA is 0.25 percent.

#### 4.6-ft Peak Power Case

With the power peaked at 4.6 feet, the cladding temperature responses resemble closely those obtained for the other two mid-core peaks. The rupture occurs at the location of peak power. The node above the rupture experiences increased cooling post rupture, and the peak cladding temperature occurs in the downstream grid span (node 11). The temperature at this location is about 100 F above the temperatures near the rupture location. The highest local oxidation, 5.2 percent, also occurs at the mid-core elevation. The whole core oxidation during this LOCA is 0.41 percent, the highest obtained in the set of LOCA limits analyses.

#### 6.3-ft Peak Power Case

For a peak power situated at the core midplane, the cladding temperature response corresponds to that described in the previous paragraph. The rupture is at the location of peak power. For this case, however, the post rupture cooling axial conduction does not outweigh the effect of the relatively lower power at the next span, and the peak cladding temperature occurs in the node just above the rupture location. As shown in Figure 8-13, the peak temperature, 1873 F, is only slightly higher, by about 70 F, than that predicted for the next higher grid span. The highest local oxidation in this case, 4.8 percent, occurs for the peak cladding temperature node. The whole core oxidation is 0.40 percent.

#### 8.0-ft Peak Power Case

Again, the temperature responses follow the pattern described for the previous two cases. Here, with the power peaked toward the outlet, the grid span that will produce high cladding temperatures lies below the location of peak power. The rupture occurs at the location of peak power and the peak cladding temperature, 1930 F, is predicted to occur in the grid span below the peak location. The markedly higher flow velocities at the higher elevations, in conjunction with rupture cooling effects and the drop-off of power, combine to produce a cladding temperature in the node above the rupture location that is nearly 200 F below the peak cladding temperature (node 12). The highest local oxidation is 4.7 percent, and the whole core oxidation is 0.32 percent.

#### 9.7-ft Peak Power Case

In accordance with the axial dependency of power peaking shown in Figure 8-1, this case is run at a slightly lower total peaking than the other four cases. The location of peak power is in node 17, which experiences some cooling due to grid effects. With the reduction in peaking and the severe outlet shape, the power in node 15 is close to that in node 17. Because the lower location, node 15, is at the end of the grid span, there is little, if any, grid effect. Thus, node 15 is the first location on the fuel pin to reach the rupture temperature. Since the rupture occurs at a node adjacent to the grid span, the rupture and spacer grid effects combine to provide better cooling in a higher powered grid span. The peak temperature, 1823 F, occurs just below the rupture location. The peak local oxidation is 3.7 percent and the whole core oxidation is 0.29 percent. (Note: The 9.7' case has been reanalyzed at a higher  $F_q$  and the results of the analysis are reported in Appendix B.)

The LOCA limits calculations directly demonstrate compliance to two of the criteria of 10 CFR 50.46 and serve as the basis for demonstrating compliance with two others. As seen in the figures and in Table 8-1, the highest peak cladding temperature, 1963 F, and the highest local oxidation, 5.2 percent, are well below the 2200 F and 17 percent criteria. Chapter 9 documents compliance with the whole core oxidation limit based on the local oxidations calculated for these evaluations, and Chapter 10 documents the core geometry based on the deformations predicted for the LOCA.



Table 8-1 LOCA Limits Results<sup>1</sup>

Item or Parameter	Elevation of Peak Power, Feet				
	2.9	4.3	6.3	8.0	9.7 <sup>2</sup>
End-of-Blowdown, s	21.0	21.3	21.2	20.7	20.8
Liquid in Reactor Vessel					
at EOB, ft <sup>3</sup>	93.1	70.2	71.8	83.9	79.0
Bottom-of-Core Recovery, s	33.0	33.7	33.5	32.8	32.9
Time of Rupture, s	81.8	74.4	67.6	73.8	84.4
Ruptured Node *	8	8	11	14	15
PCT at Rupture Node, F	1611	1669	1666	1655	1602
Oxide at Rupture Node, %	3.4	3.5	4.8	1.5	0.8
Node Adjacent to					
Rupture *	9	9	12	15	14
PCT of Adjacent Node, F	1804	1839	1873	1753	1823
Oxide at Adjacent Node, %	2.9	3.1	4.4	3.0	3.2
Node in Adjacent					
Grid Span *	11	11	14	12	12
PCT of Adjacent Grid					
Span, F	1816	1963	1805	1930	1718
Oxide at Adjacent					
Grid Span, %	3.0	5.2	3.4	4.7	2.0
Pin PCT Node *	11	11	12	12	14
Peak Local Oxidation, %	3.4	5.2	4.8	4.7	3.7
Whole Core Oxidation, %	0.25	0.41	0.40	0.32	0.29

\* Refer to Figure 4-4 for noding arrangement.

<sup>1</sup> See the responses to question numbers 13 and 30 on BAW-10174, the response to question number 2 on BAW-10168, Revision 1, and the response to question number 5 on BAW-10166, Revision 2.

<sup>2</sup> The 9.7' case has been reanalyzed at a higher  $F_0$  and the results of the analysis are reported in Appendix B.

FIGURE 8-1 AXIAL DEPENDENCE OF ALLOWED TOTAL PEAKING FACTOR  
LARGE BREAK LOCA MARK-BW

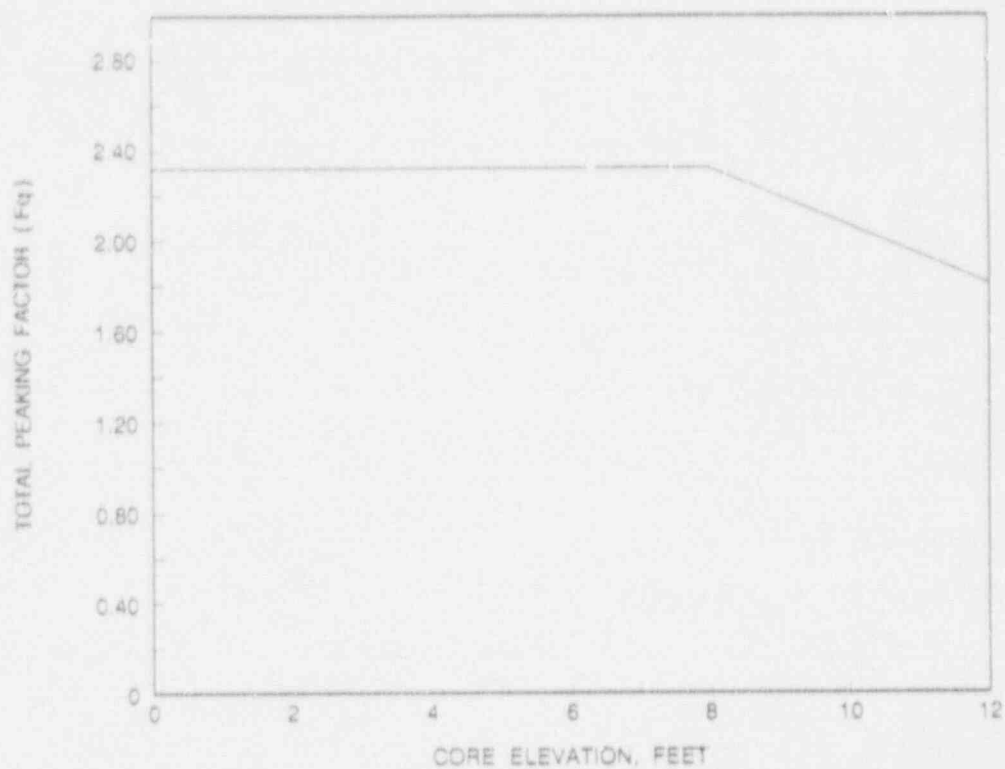


FIGURE 8-2 NORMALIZED LOCAL POWER BURNUP DEPENDENCY FACTOR

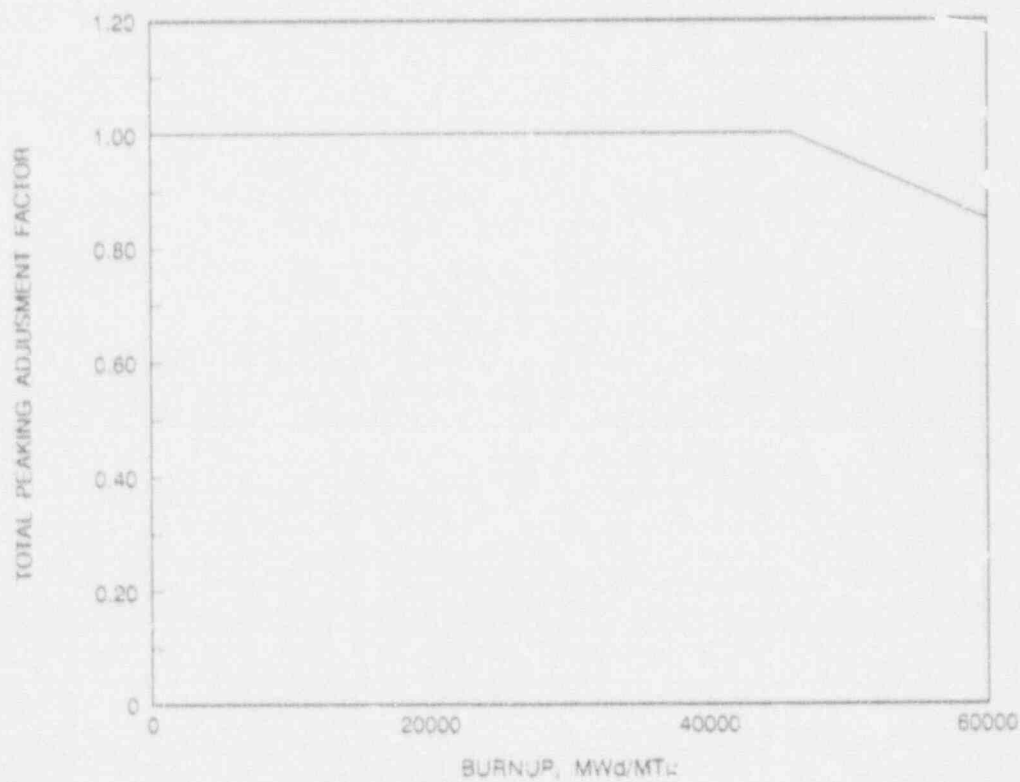


FIGURE 8-3 LOCA LIMIT STUDY - AXIAL POWER SHAPES

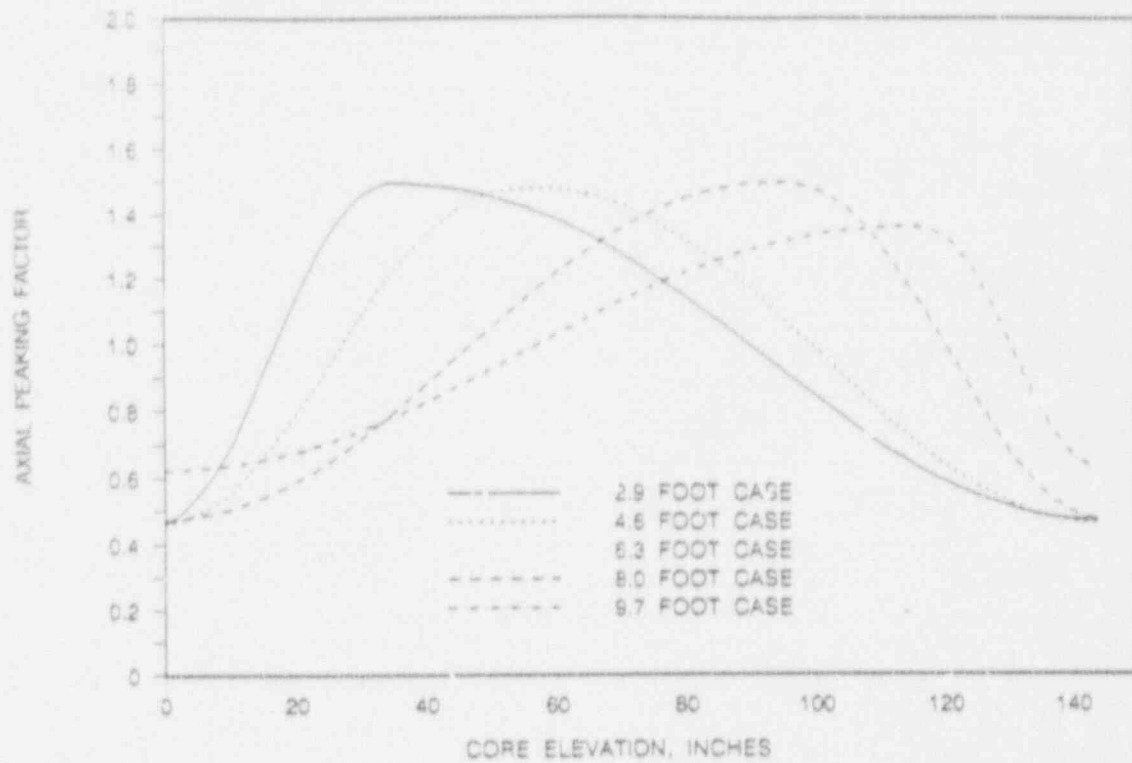


FIGURE 8-4 LOCA LIMITS STUDY - 2.9 FOOT CASE  
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

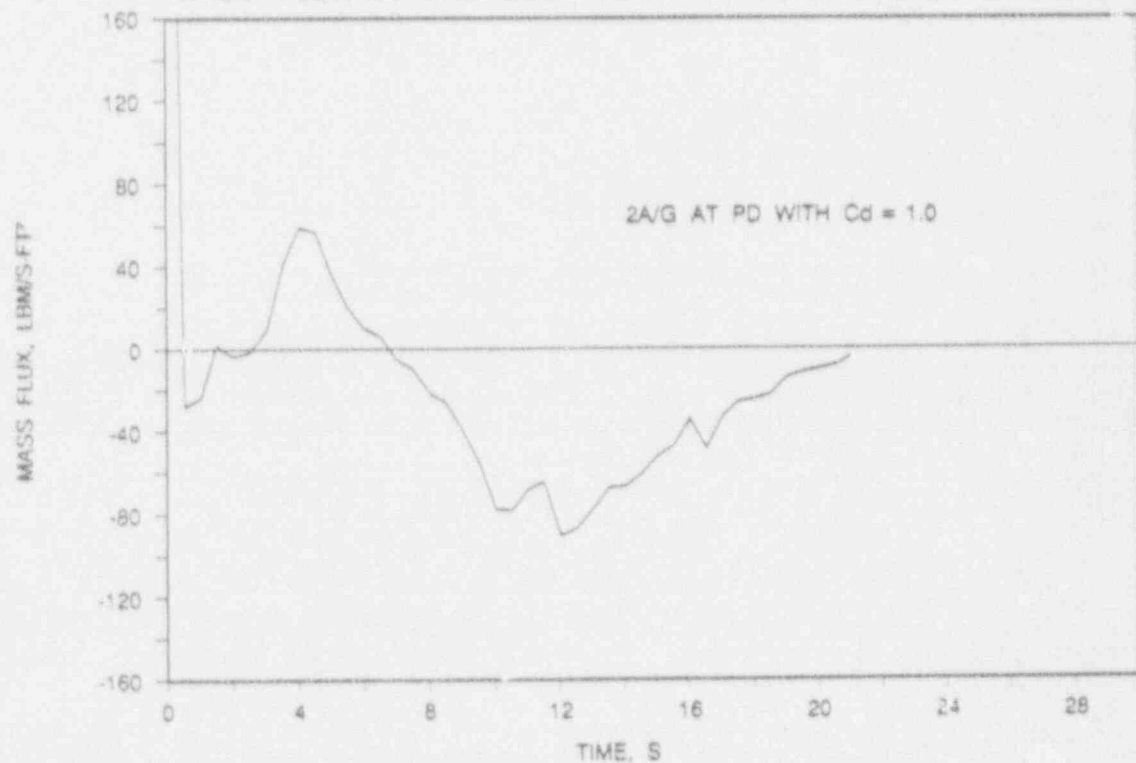


FIGURE 8-5 LOCA LIMITS STUDY - 2.9 FOOT CASE  
CLADDING TEMPERATURES

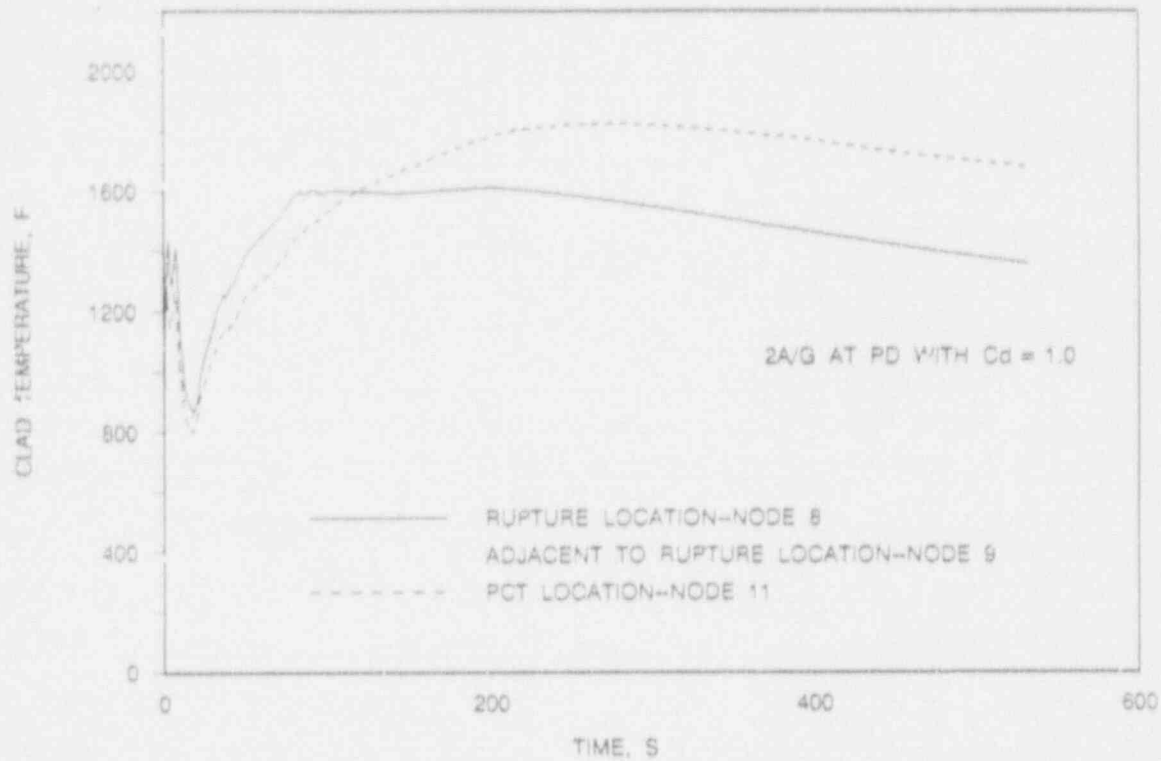


FIGURE 8-6 LOCA LIMITS STUDY - 2.9 FOOT CASE  
HEAT TRANSFER COEFFICIENT AT PCT LOCATION

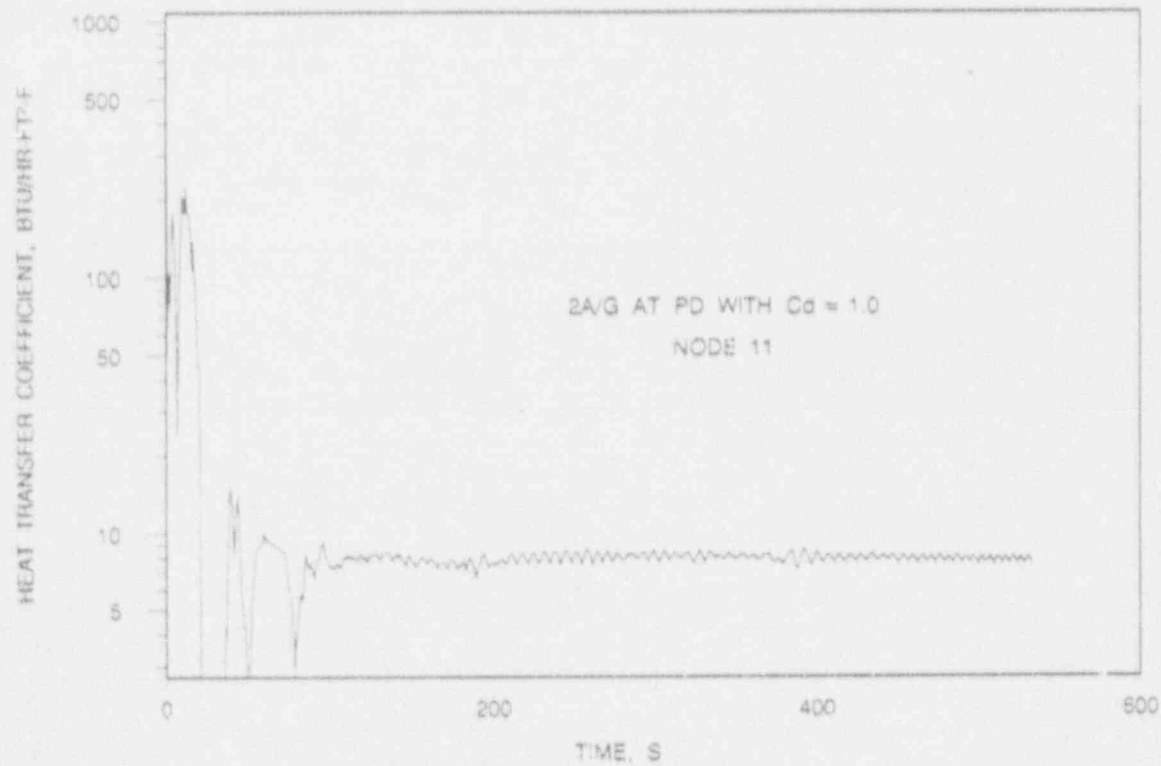


FIGURE 8-7 LOCA LIMITS STUDY - 2.9 FOOT CASE  
LOCAL OXIDATION

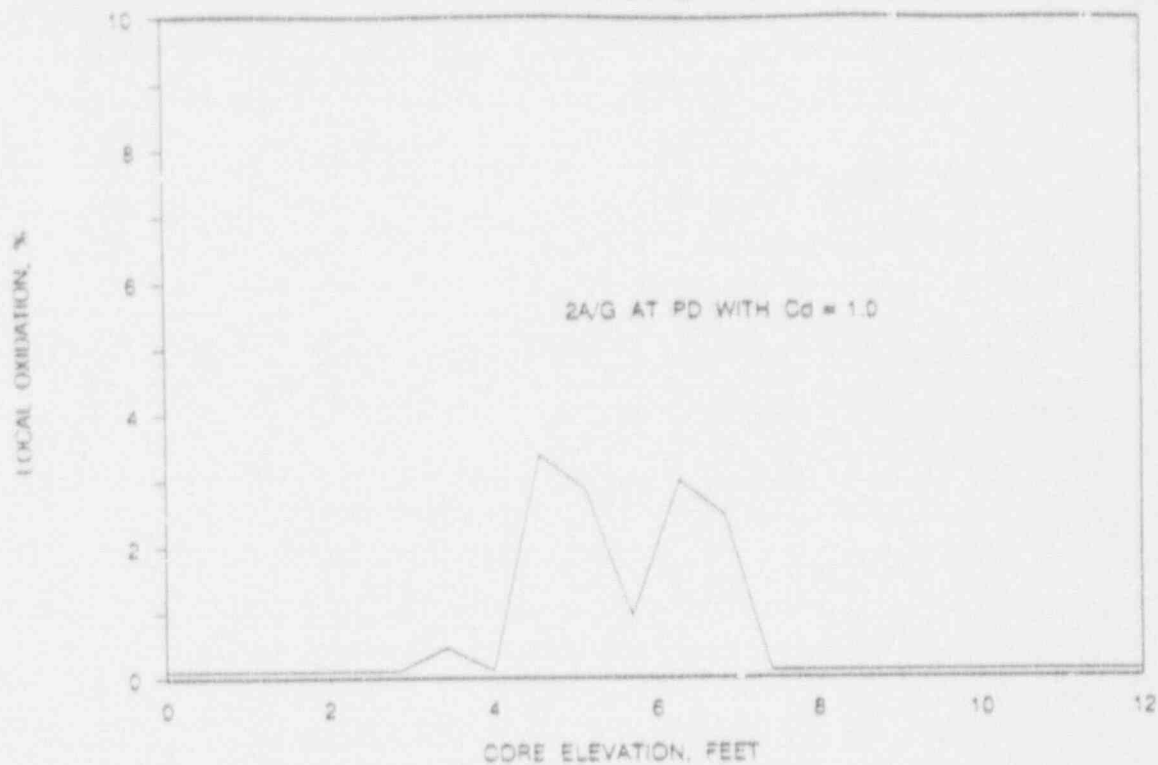


FIGURE 8-8 LOCA LIMITS STUDY - 4.6 FOOT CASE  
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

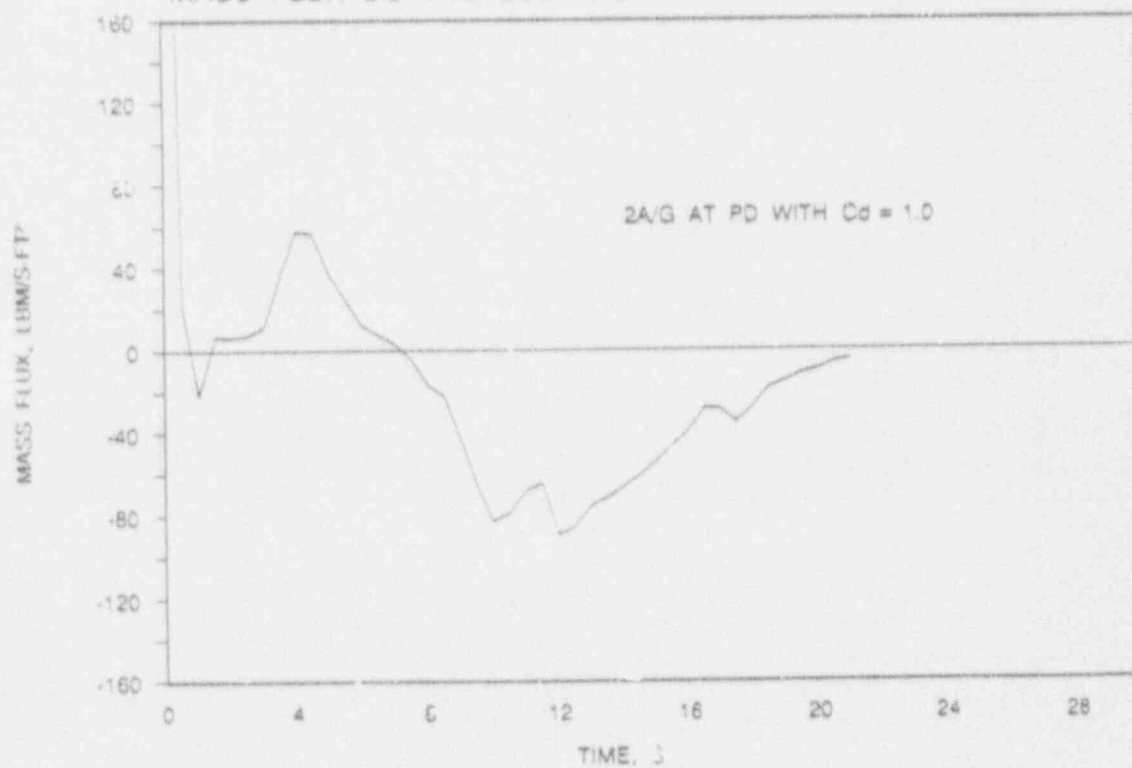


FIGURE 8-9 LOCA LIMITS STUDY - 4.6 FOOT CASE  
CLADDING TEMPERATURES

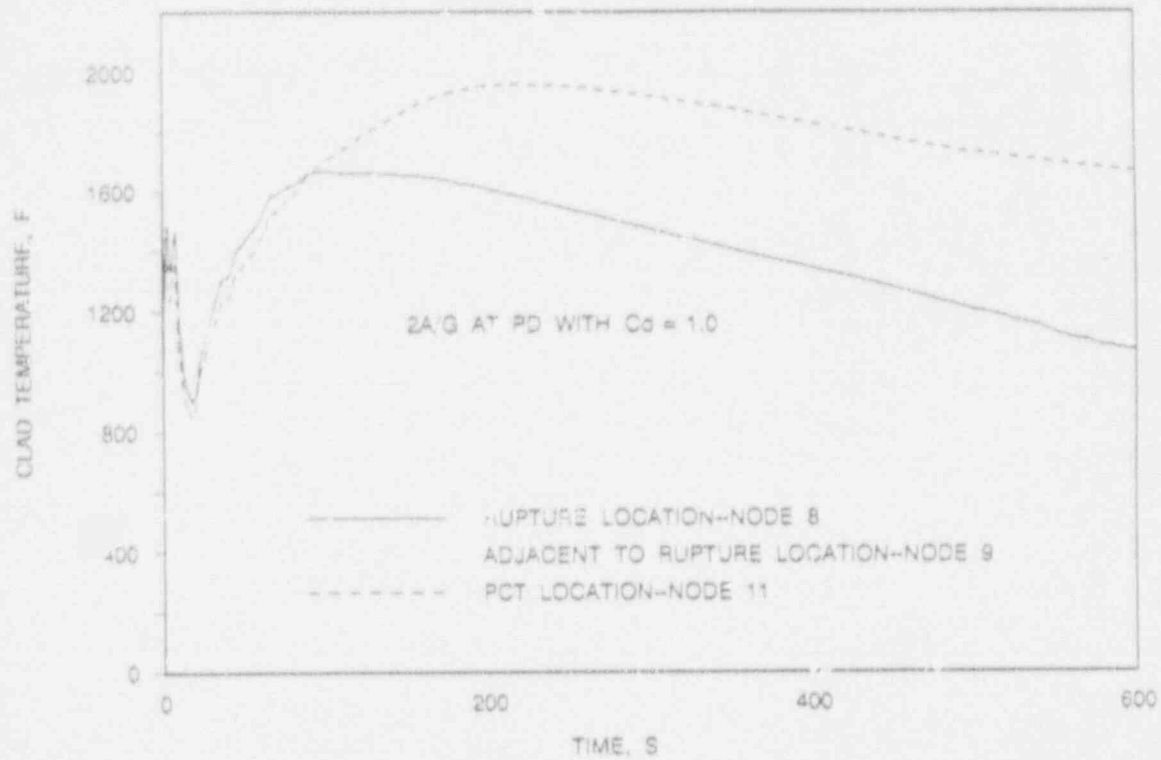


FIGURE 8-10 LOCA LIMITS STUDY - 4.6 FOOT CASE  
HEAT TRANSFER COEFFICIENT AT PCT LOCATION

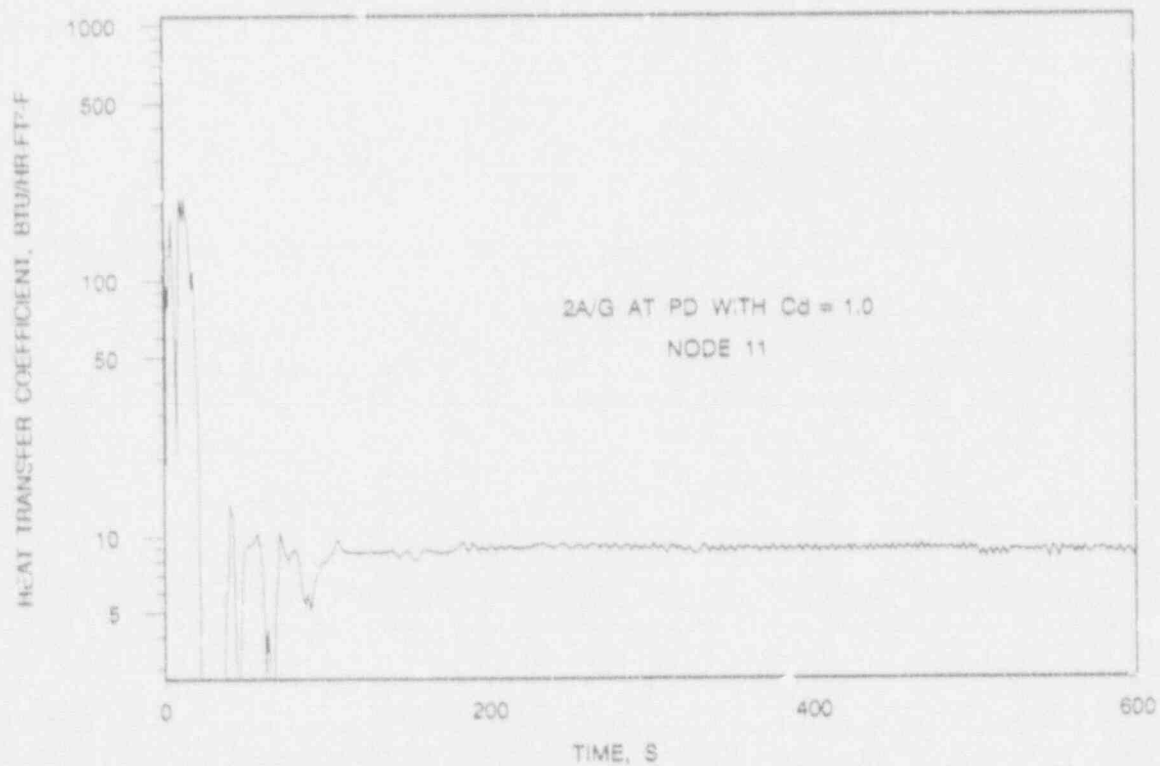




FIGURE 8-11 LOCA LIMITS STUDY - 4.6 FOOT CASE  
LOCAL OXIDATION

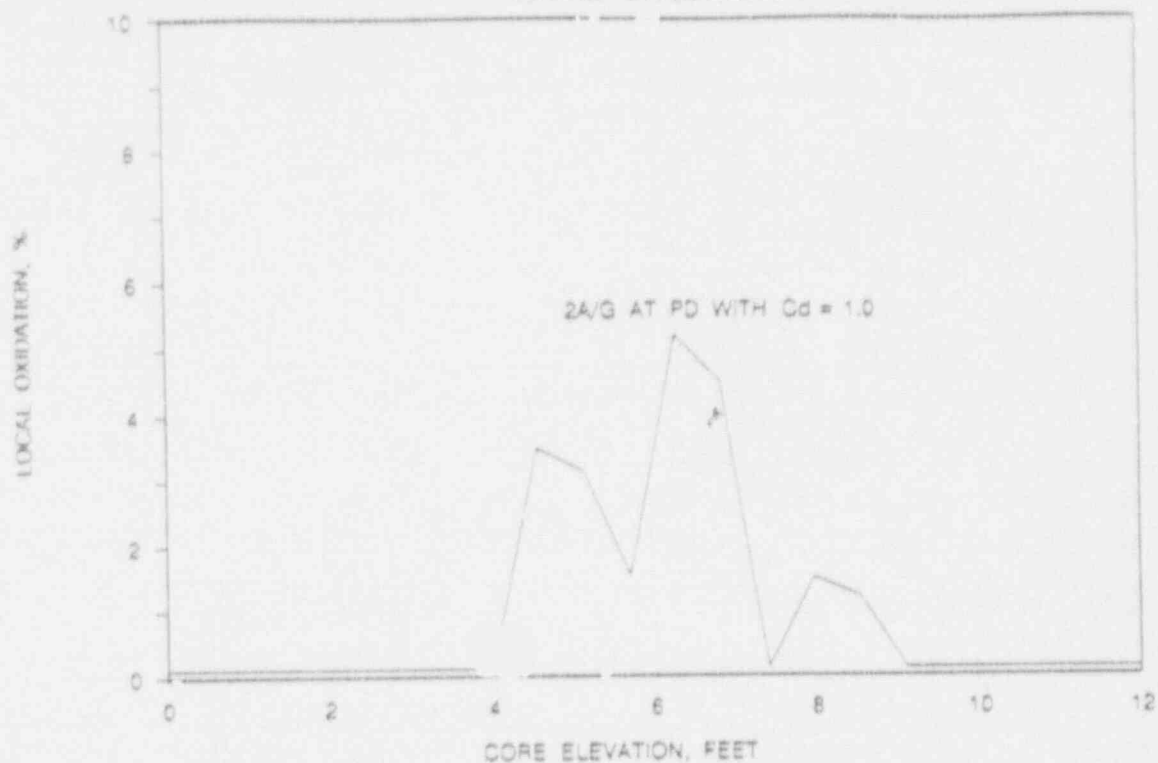


FIGURE 8-12 LOCA LIMITS STUDY - 6.3 FOOT CASE  
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

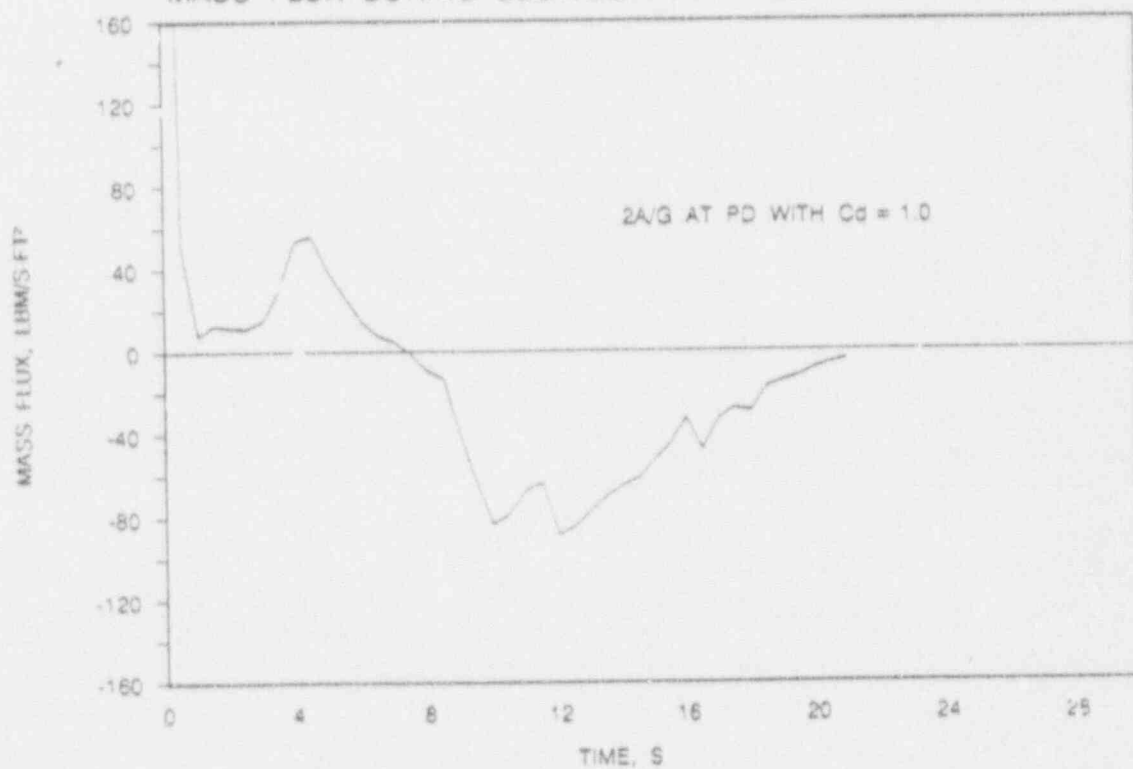


FIGURE 8-13 LOCA LIMITS STUDY - 6.3 FOOT CASE  
CLADDING TEMPERATURES

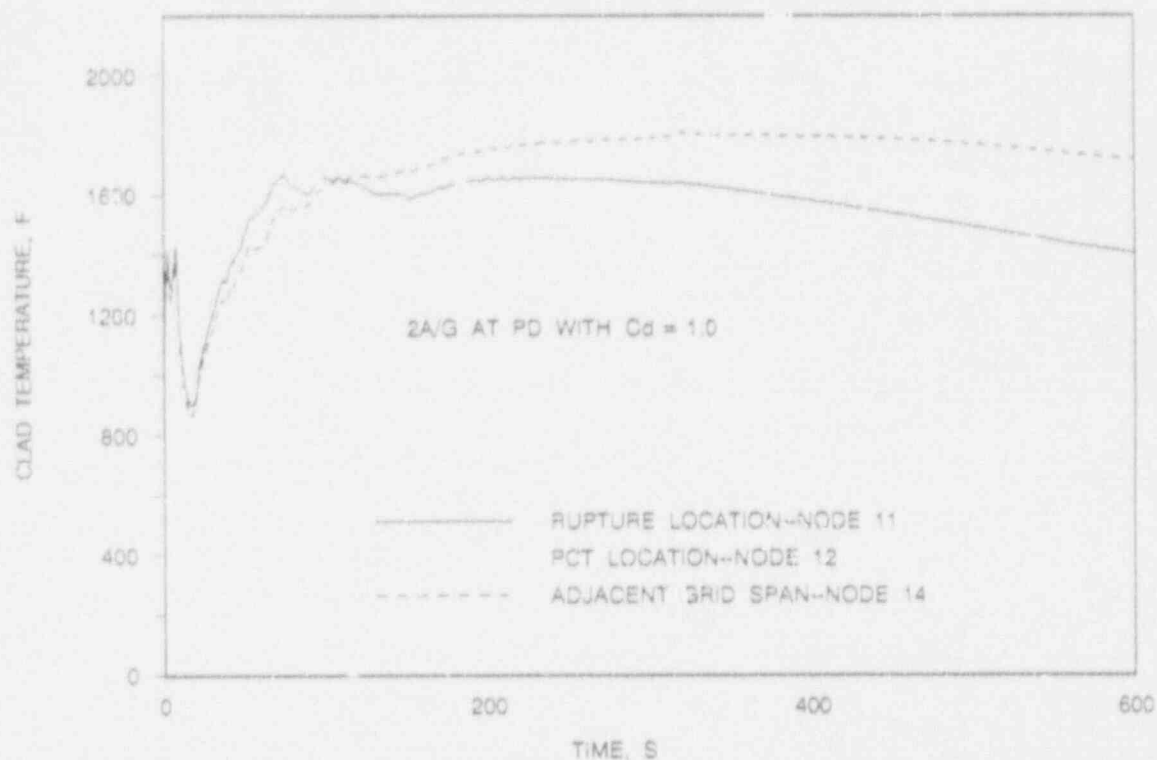


FIGURE 8-14 LOCA LIMITS STUDY - 6.3 FOOT CASE  
HEAT TRANSFER COEFFICIENT AT PCT LOCATION

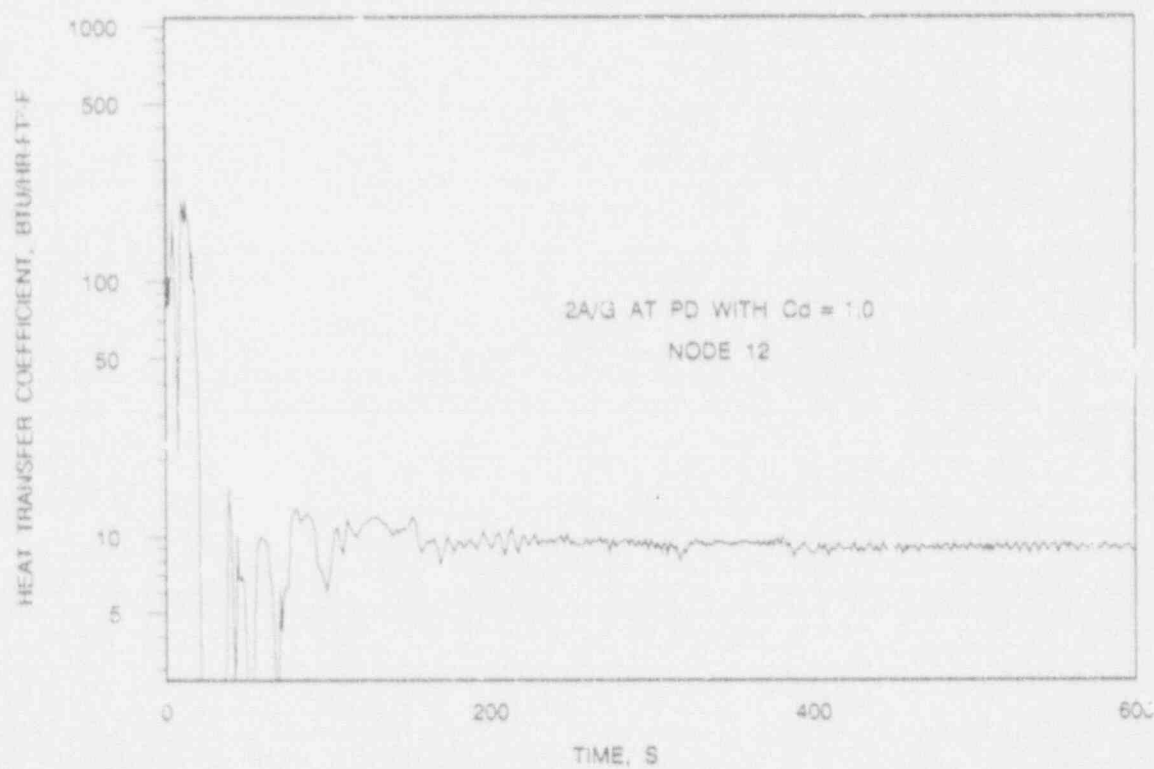


FIGURE 8-15 LOCA LIMITS STUDY - 6.3 FOOT CASE  
LOCAL OXIDATION

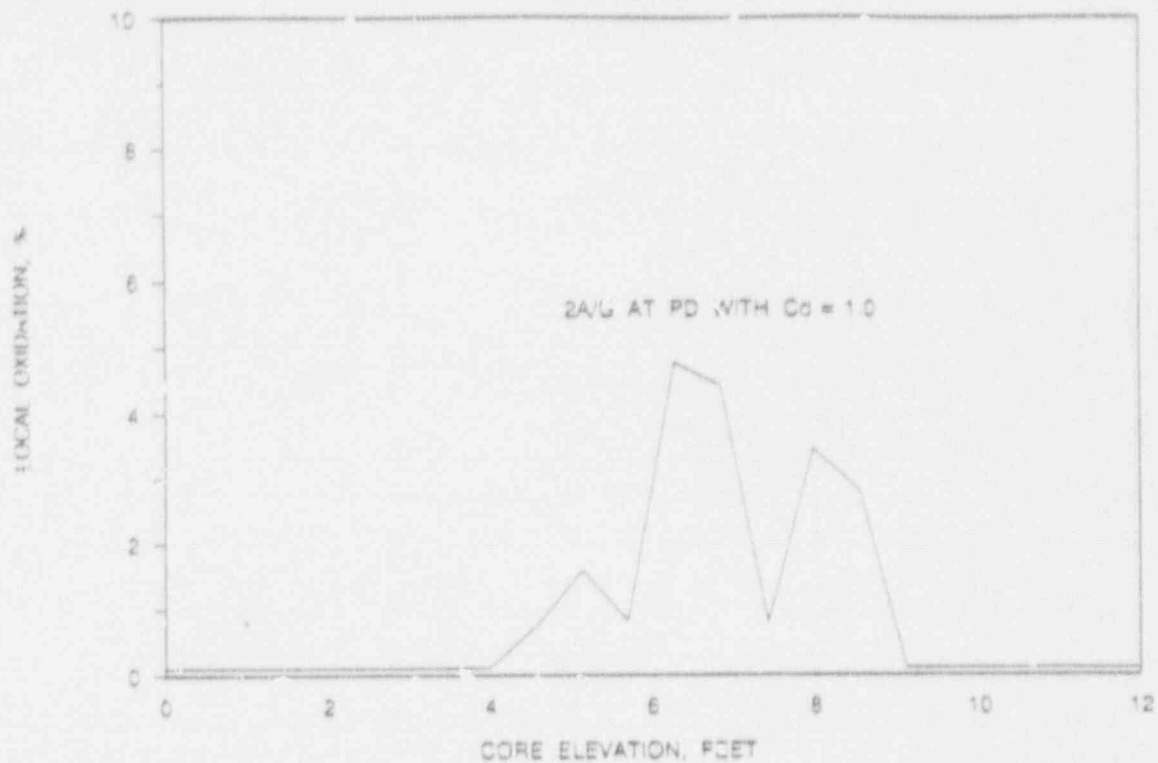


FIGURE 8-16 LOCA LIMITS STUDY - 8.0 FOOT CASE  
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

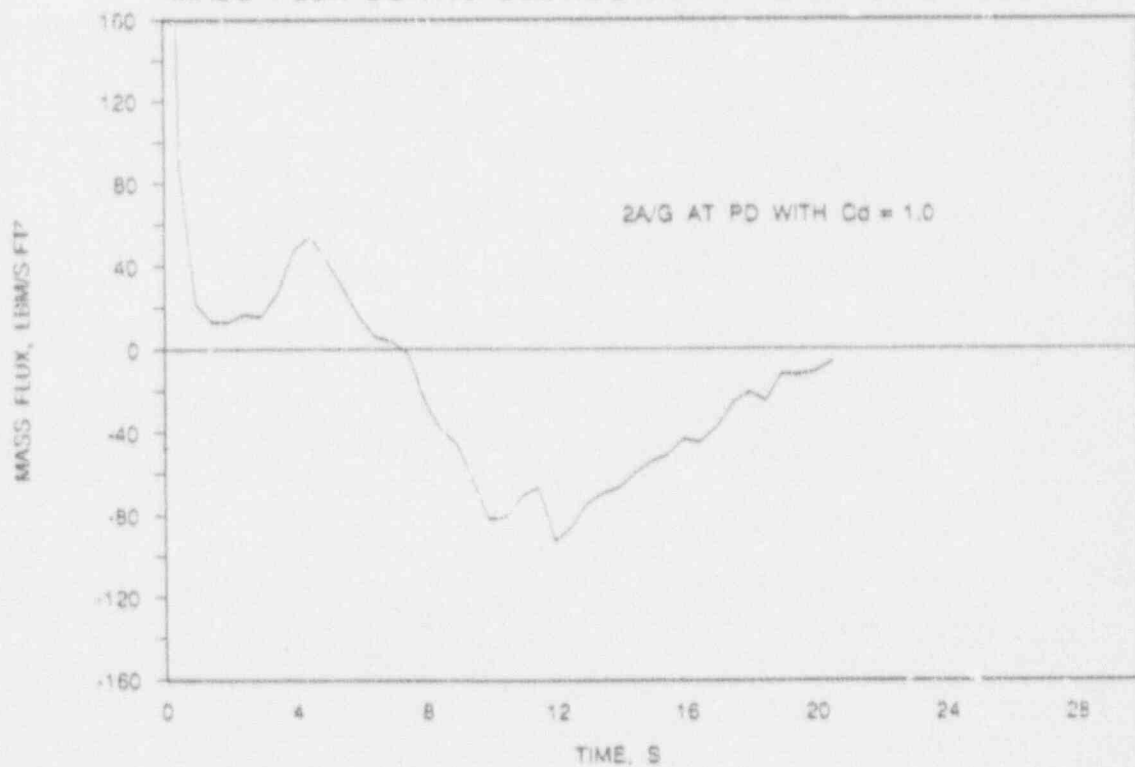


FIGURE 8-17 LOCA LIMITS STUDY - 8.0 FOOT CASE  
CLADDING TEMPERATURES

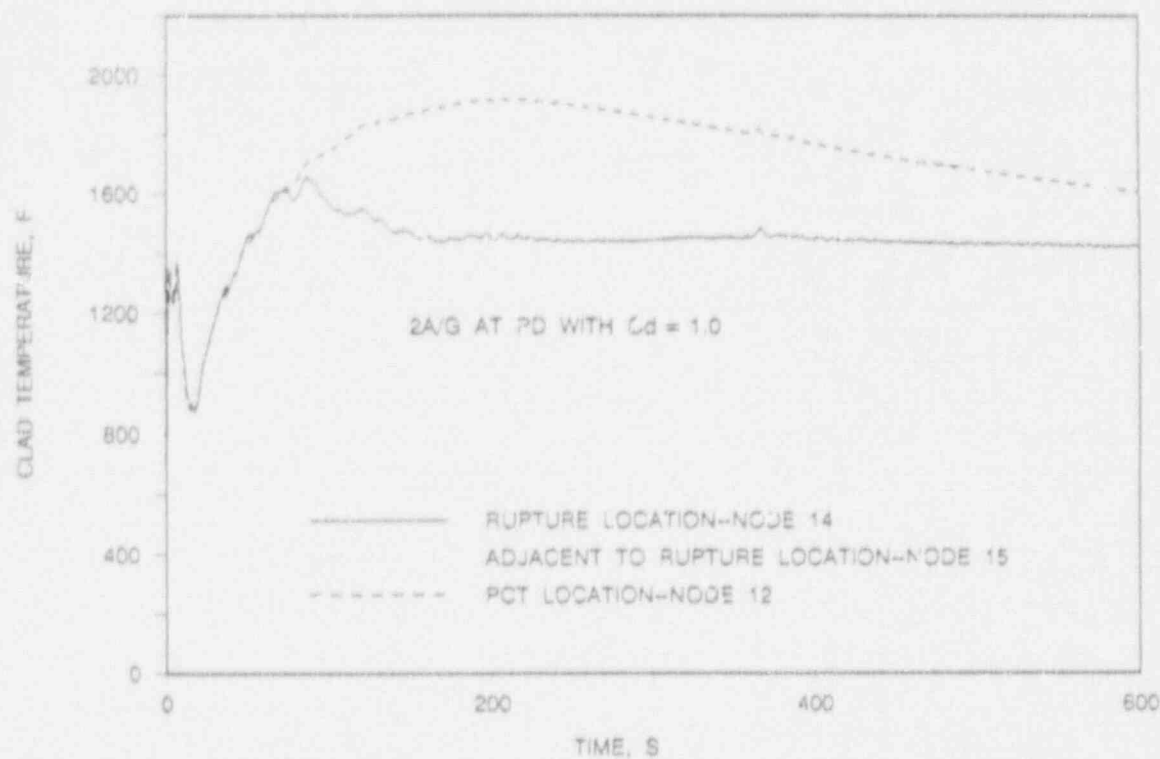


FIGURE 8-18 LOCA LIMITS STUDY - 8.0 FOOT CASE  
HEAT TRANSFER COEFFICIENT AT PCT LOCATION

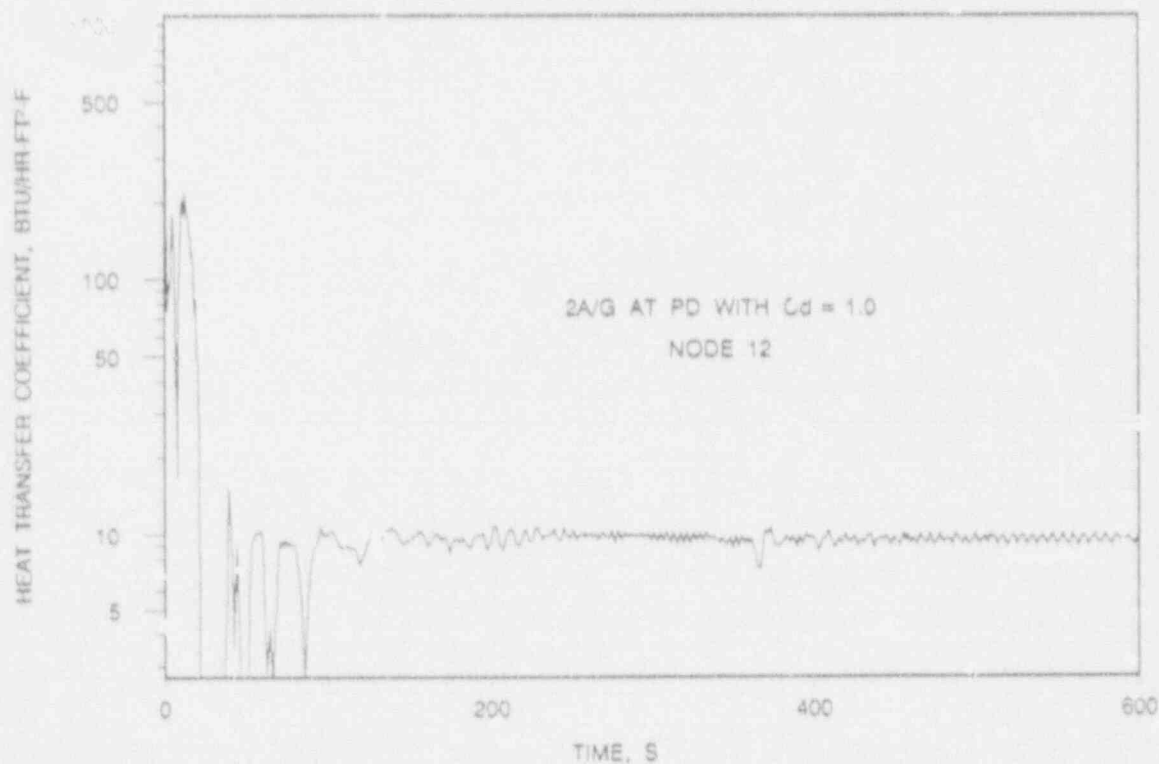


FIGURE 8-19 LOCA LIMITS STUDY - 9.0 FOOT CASE  
LOCAL OXIDATION

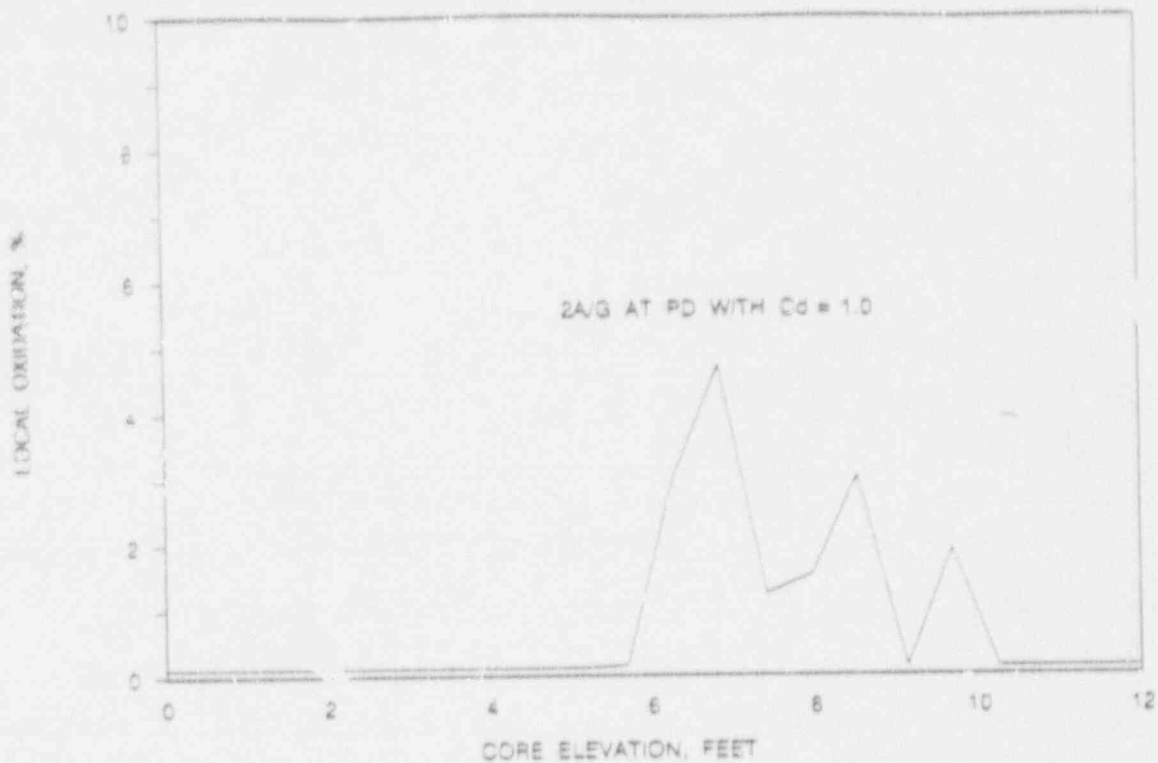


FIGURE 8-20 LOCA LIMITS STUDY - 9.7 FOOT CASE  
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

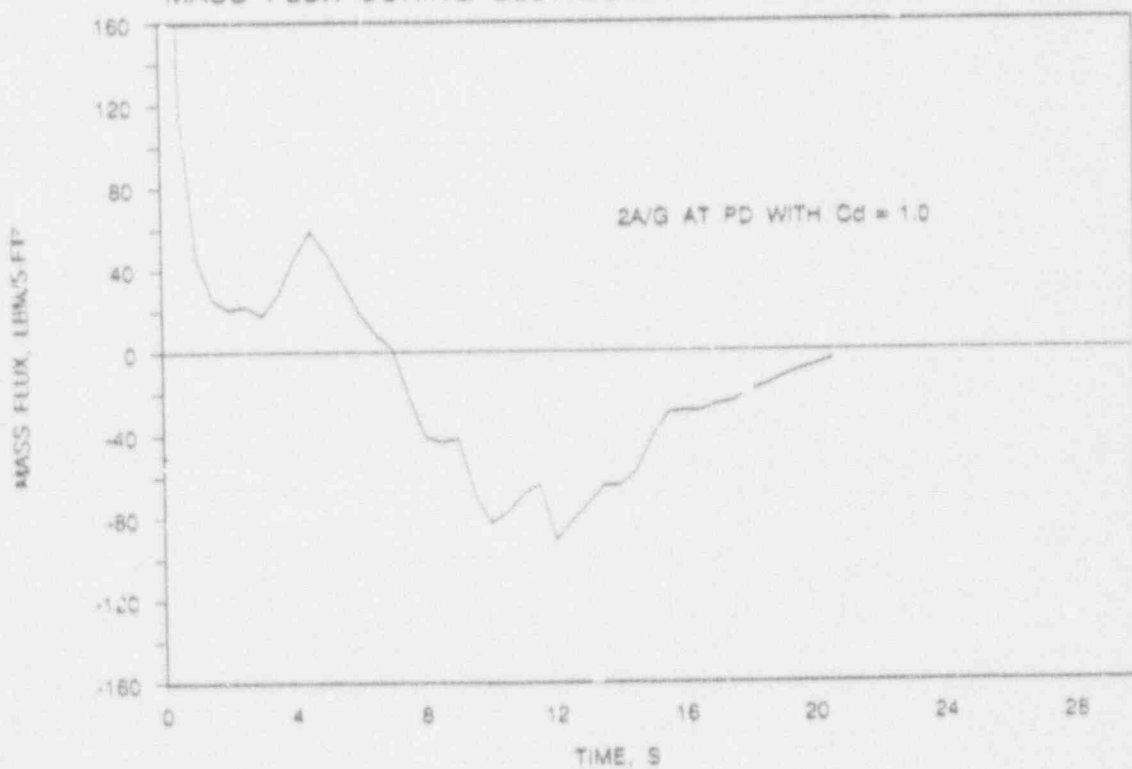


FIGURE B-21 LOCA LIMITS STUDY - 9.7 FOOT CASE  
CLADDING TEMPERATURES

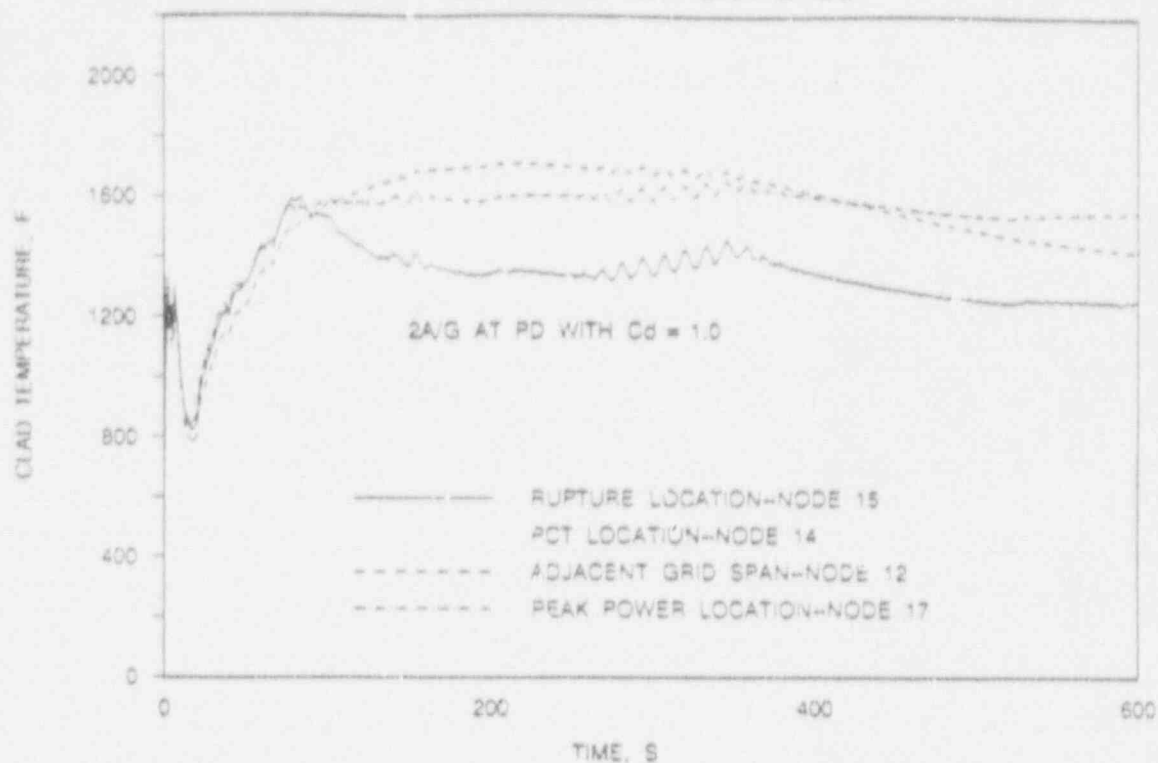


FIGURE B-22 LOCA LIMITS STUDY - 9.7 FOOT CASE  
HEAT TRANSFER COEFFICIENT AT PCT LOCATION

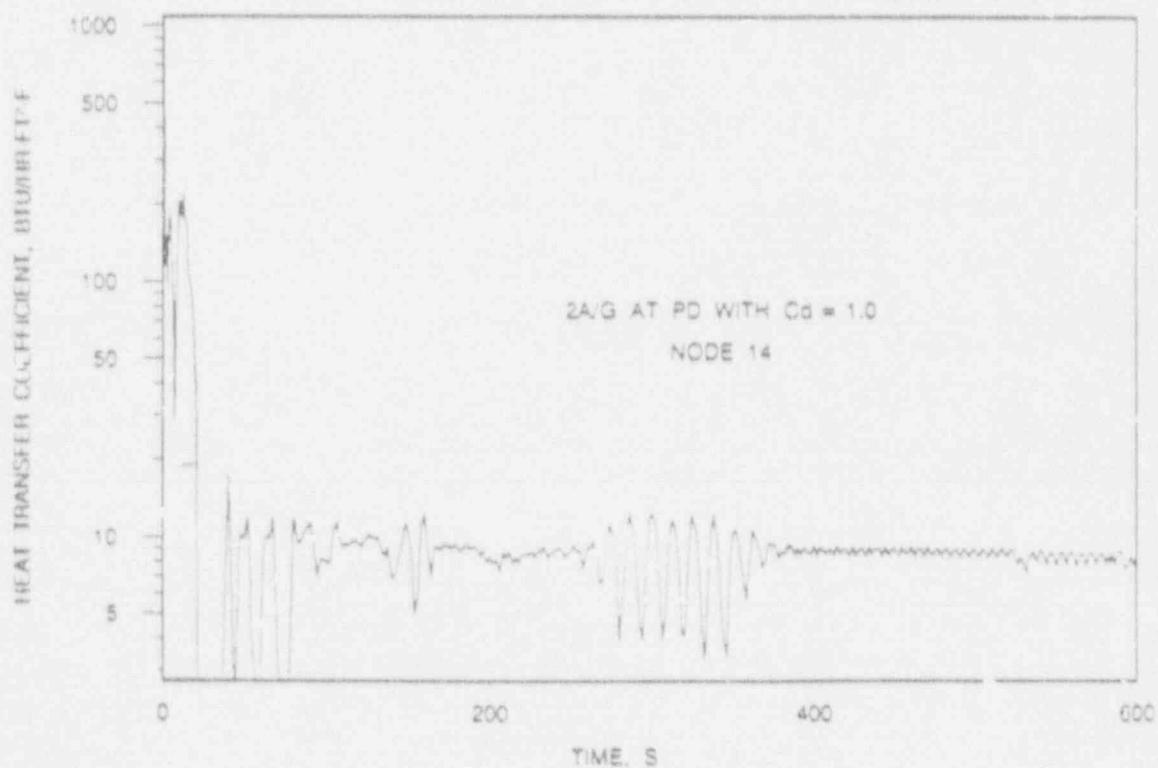
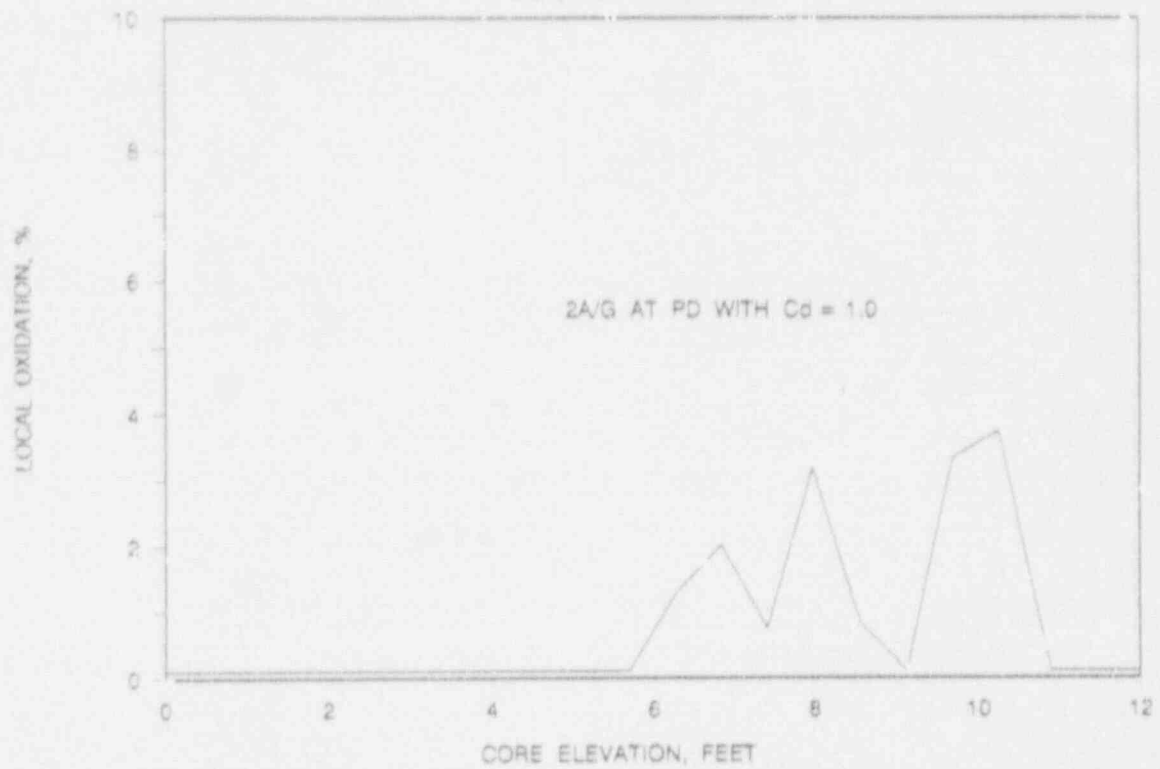




FIGURE 8-23 LOCA LIMITS STUDY - 9.7 FOOT CASE  
LOCAL OXIDATION



## 9. Whole-Core Oxidation and Hydrogen Generation

The third criterion of 10 CFR 50.46 states that the calculated total amount of hydrogen generated from the chemical reaction of the cladding with water or steam shall not exceed 0.01 times the hypothetical amount that would be generated if all of the metal in the cladding cylinders surrounding the fuel, excluding the cladding surrounding the plenum volume, were to react. The method provided in the BWFC evaluation model, Reference 1, has been applied to determine corewide oxidation for each of the LOCA limits cases. In the calculations, local cladding oxidation was computed as long as the cladding temperature remained above 1500 F, and the REFLOD3B analysis did not show that the cladding was within the core flooded region. The flooded region of the core was conservatively taken to be twenty percent above the core collapsed liquid level. These local oxidations are summed over the core to give the core-wide oxidation. The figures in Chapter 8 give the local oxidation for the hot pin including the initial oxide layer. The only difference between these distributions and the ones used for the whole core calculation is that the initial oxide layer is subtracted before the integration in order to provide a measure of the hydrogen produced during the LOCA. The results of these calculations for each of the power distributions of the LOCA Limits cases are:

<u>Case</u>	<u>Whole Core Oxidation, %</u>
2.9-ft Peak	0.25
4.6-ft Peak	0.41 <sup>1</sup>
6.3-ft Peak	0.40
8.0-ft Peak	0.32
9.7-ft Peak	0.29 <sup>2</sup>

<sup>1</sup> See the response to question number 13 on BAW-10174.

<sup>2</sup> The 9.7' case has been reanalyzed at a higher  $F_c$  and the results of the analysis are reported in Appendix B.

Because these cases represent a range of the possible power distributions that can occur in the plant, the maximum possible oxidation that can occur during a LOCA at the McGuire or Catawba plants is calculated to be less than 0.41 percent. Thus, the third criterion of 10 CFR 50.46, which limits the reaction to 1 percent or less, is met with considerable margin.

## 10. Core Geometry

The fourth acceptance criterion of 10 CFR 50.46 states that calculated changes in core geometry shall be such that the core remains amenable to cooling. The calculations in Chapter 8 directly assess the alterations in core geometry, which result from the LOCA, at the most severe location in the core. These calculations demonstrate that the fuel pin cooled successfully. As discussed in Section 7 of the original BWFC evaluation model report (Reference 1), clad swelling and flow blockage due to rupture can be estimated based on NUREG-0630. For the McGuire and Catawba plants, the hot assembly flow area reduction at rupture is less than 60 percent for all LOCA limits cases. Furthermore, the upper limit of possible channel blockage, based on NUREG-0630, is less than 90 percent. Neither 90 percent blockage nor 60 percent blockage constitutes total subchannel obstruction. As the position of rupture in a fuel assembly is distributed within the upper part of a grid span, subchannel blockage will not become coplanar across the assembly. Therefore, the assembly retains its pin - coolant channel - pin - coolant channel arrangement and is capable of passing coolant along the pin to provide cooling for all regions of the assembly.

The effects of fuel rod bowing on whole core blockage are considered in the BWFC fuel assembly and fuel rod designs, which minimize the potential for rod bowing. The minor adjustments of fuel pin pitch due to rod bowing do not alter the fuel assembly flow area substantially and the average subchannel flow area is preserved. Therefore, due to the axial distribution of blockage caused by rupture, no coplanar blockage of the fuel assembly will occur and the core will remain amenable to cooling. Deformation of the fuel pin lattice at the core periphery can occur from the combined mechanical loadings of the LOCA and a seismic event. These loads have been analyzed separately in the original plant structural designs to ensure that they have no adverse effect on

the core cooling processes. The loadings and effect on the Mark-BW assembly are presented in the Mark-BW fuel mechanical design report (BAW-10172, Reference 7). Although deformations can occur, they are limited to the outer two or three points on the lattice structure of the core and do not cause a subchannel flow area reduction larger than 35 percent. The fuel pins at these lattice points do not operate at power levels sufficient to produce a cladding rupture during LOCA. Therefore, the only reduction in channel flow area is from the mechanical effect, and the assemblies retain a coolable configuration.

The consequences of both thermal and mechanical deformation of the fuel assemblies in the core have been assessed and the resultant deformations have been shown to maintain coolable core configurations. Therefore, the coolable geometry requirements of 10 CFR 50.46 have been met and the core has been shown to remain amenable to core cooling.

## 11. Long-Term Cooling

The fifth acceptance criterion of 10 CFR 50.46 states that the calculated core temperature shall be maintained at an acceptably low value, and decay heat shall be removed for the extended period of time required by the long-lived radioactivity remaining in the core. Successful initial operation of the ECCS is shown by demonstrating that the core is quenched and the cladding temperature is returned to near saturation temperature. Thereafter, long-term cooling is achieved by the pumped injection systems. These systems are redundant and able to provide a continuous flow of cooling water to the core fuel assemblies so long as the coolant channels in the core remain open. For a cold leg break, the concentration of boric acid within the core might induce a crystalline precipitation which could prevent the coolant flow from reaching certain portions of the core. This chapter presents the evaluation of the final stages of the initial operation of the ECCS, a discussion of the long-term supply of water to the core, and a discussion of the procedures to prevent the build-up of boric acid in the core.

### 11.1 Initial Cladding Cooldown

The heat transfer models used to determine the peak cladding temperature are most conservative following the initiation of cladding cooldown and cannot be used directly to predict cladding quench. Rather, the occurrence of core quenching is determined from the core liquid inventory, as predicted by the REFLOD3B code, with a conservatively assumed 20 percent mixture swell. After quenching, core heat transfer is by pool nucleate boiling or by forced convection to liquid, depending on the location of the break in the reactor coolant system (cold leg breaks are in pool nucleate and hot leg breaks in forced convection). Either mechanism is fully capable of maintaining the core within a few



degrees of the saturation temperature of the coolant. Thus, within ten to fifteen minutes of the LOCA, the core has been returned to an acceptably low temperature level.

### 11.2 Extended Coolant Supply

Once the core has been cooled to low temperatures, maintaining that condition relies upon the systems available to provide a continuous supply of coolant to the core. Detailed descriptions of the plant systems and functions are provided in the safety analysis reports for the McGuire and Catawba units (References 8 and 9). Provision for long-term core cooling with the ECCS as demonstrated in the FSARs is independent of the fuel design. Thus, the licensing basis for previous operation remains valid for Mark-BW reload fuel.

### 11.3 Boric Acid Concentration

As discussed, the long-term cooling mechanism for a hot leg break is by forced convection to liquid: once established, the coolability of the core is assured and need not be further considered. For the cold leg break, however, there is no forced flow through the core. The liquid head balances between the core and the downcomer prevent ECCS water from entering the core at a rate faster than the rate of core boiling. Extra injection simply flows out of the break and spills to the containment. With no throughput, the boiling in the core region acts to concentrate boric acid. To limit the concentration of boric acid, the operator is required to establish a hot leg recirculation mode of operation within 15 hours of the initiation of the accident.

In this mode, the piping is aligned so that injection takes place in both the hot and cold legs. By doing so, the amount of injection to the hot leg becomes a through-flow that can control

the concentration of boric acid. The timing and effectiveness of the hot leg injection is established by demonstrating that the in-vessel concentrations are well below the solubility limits for the dissolved solids. Therefore, there is no dependency on the fuel element design as the concentrations depend only on the injection rate, the Reactor Coolant System geometry, and the core power level. Since none of these factors have been altered by the fuel change, the evaluation in the referenced FSARs remains valid for the plant. The operator actions and procedures to establish the operation are also described in the FSARs.

#### 11.4 Adherence to Long-Term Cooling Criterion

Compliance to this criterion is demonstrated for the systems and components specific to the Catawba and McGuire units in the referenced FSARs and is not related to the fuel design. The initial phase of core cooling has been shown to result in low cladding and fuel temperatures. A pumped injection system capable of recirculation is available and operated by the plant to provide extended coolant injection. The concentration of dissolved solids has been shown to be limited to acceptable levels through the timely implementation of hot leg recirculation. Therefore, the capability of long-term cooling has been established and compliance to 10 CFR 50.46, demonstrated.

## 12. Small Break LOCA

The current licensing bases for the McGuire and Catawba plants comprise a spectrum of large and small break loss-of-coolant accidents (LOCAs) analyzed by Westinghouse and documented in Chapter 15 of each of the plant final safety analysis reports (FSARs). For operation of Catawba and McGuire with BWFC-supplied fuel, BWFC has reanalyzed the large break LOCA transient as presented in the foregoing chapters. Reanalysis was considered necessary for two reasons: (1) for Catawba and McGuire, the large break is clearly the limiting loss-of-coolant event, and (2) the large break results can be sensitive to changes in fuel design. By these same considerations, reanalysis of the small break LOCA for operation with Mark-BW reload fuel is not required; the current SBLOCA results are not the most limiting or severe LOCA cases, and SBLOCA evaluations are unaffected by the design differences between the Mark-BW and resident Westinghouse fuel assemblies. Thus, the referenced FSAR analyses, performed by Westinghouse, remain the bases for plant licensing even after the cores are loaded with BWFC-supplied fuel assemblies.

### 12.1 SBLOCA Transient

SBLOCA transients can be generally characterized as developing in five distinct phases: (1) subcooled depressurization, (2) pump/loop flow coastdown and natural circulation, (3) loop draining, (4) vessel/core boil-off, and (5) long-term cooling. These phases are examined in the following paragraphs as a lead-in to a discussion in the next section of the effects, if any, of fuel design differences--between the resident Westinghouse fuel and the Mark-BW reload assemblies--upon the sequence of events and consequences of the small break LOCA transient for the Catawba and McGuire units.

The limiting SBLOCA events begin with a subcooled reactor coolant system (RCS) depressurization until the primary system pressure reaches the initial hot leg temperature saturation pressure. During this depressurization phase, the low pressure reactor trip, ECCS injection, and reactor coolant pump trip signals are generated. Tripping of the pumps begins the pump and loop flow coastdown period.

Following reactor trip, the core power drops sharply. The initial forced flow and subsequent coastdown flow provide continuous heat removal via the steam generators. Thus, the initial stored energy and the core power and decay heat during this phase are transferred directly to the steam generators. The pump coastdown and natural circulation flows during this period are sufficient to prevent critical heat flux (CHF) from occurring in the core. As a result, the fuel pins are cooled toward the quasi-steady temperature distribution required to simply conduct and convect the decay heat energy out of the pins. These pin temperatures approach the RCS saturation temperature. Loss of continuous loop flow marks the end of this period.

The third phase in the transient is characterized as a period of loop draining. During this period, the system reaches a quiescent state in which the core decay heat, leak flows, pumped ECCS injection, and steam generator heat transfer combine to control the development of steam-water mixture levels within the RCS. The system inventory distribution is a strong function of the system geometry and break location. RCS liquid inventory will continue to decrease until component mixture levels provide a continuous vent path for core steam production. Relief of core steam production allows the RCS to further depressurize and enter the boil-off mode.

The development and timing of events that mark the end of the loop draining and onset of core boil-off are governed by the

break location. For hot leg breaks, the continuous core steam venting path is readily established. A significant system inventory loss is required to establish the vent path for steam or downstream piping breaks. The most severe of all occurs in the cold leg pump discharge breaks. In these breaks liquid inventory is lost until the primary levels descend to the spill-under elevation at the low point in the cold leg suction piping. This liquid trap or loop seal must be cleared of liquid to establish the steam venting path to the leak. Since the loop seal elevation is located slightly above the middle of the core, the core collapsed liquid level will be depressed by the manometric pressure balance imposed by the RCC geometry. Once the loop seals clear, the steam venting path is established and the residual liquid inventory in the pump discharge and downcomer regions drains into the core region.

The onset of the boil-off period typically coincides with the beginning of a final saturated depressurization. Voiding at the break increases the leak volumetric flow rate which ultimately depressurizes the system until the accumulator fill pressure is reached or the pumped ECCS injection matches core steaming. During this period, the reactor vessel mixture levels may drop into the core heated region. Pin temperature excursions calculated for the upper elevations are maximized by the assumption of a bounding, core outlet-skewed peaking. During these heatups, the cladding may swell and even rupture if the temperature approaches 1500 F. However, as long as the cladding temperature remains below 1800 F, the occurrence of rupture is treated conservatively by not including the effects of rupture in the evaluation.

Swelling and rupture produce three primary effects on the temperature calculation. First, the cladding expansion increases the fuel pin gap allowing a momentary cooling of the clad. This condition is temporary; however, it delays the temperature



excursion, resulting in a lower peak cladding temperature because the decay heat level has decreased slightly. Secondly, the rupture may divert flow out of the channel. The evaluation model uses only average channel flows to cool the hot channel. This flow is significantly lower than that expected in the hot channel. Thus, the effects of rupture-induced flow diversion are already conservatively bounded by the modeling. Finally, the inside of the cladding is exposed to metal-water reaction which creates a new heat source. The metal-water reaction is exponential with the cladding temperature, becoming significant relative to decay heat levels as the temperature approaches 1800 F. Below 1800 F the metal-water heating is a small fraction of the decay heat. The extra heat from the inside of the cladding need not be considered to conservatively evaluate the cladding temperature so long as that temperature remains below 1800 F.

The temperature excursions are arrested as the combined ECCS flows exceed the core decay heat level and final core refill begins. The suppression of core steam production further depressurizes the RCS, and thus increases the ECCS injection flow and hastens core refill. Eventually the RCS system will be depressurized to the containment pressure and the core will be refilled. At this point, the start of a long-term cooling configuration has been established and the transient is mitigated.

## 12.2 Fuel Design Effects

SBLOCA transients are affected primarily by system design and core decay heat levels. Fuel assembly design influences the calculated sequence of events only to the extent that it affects overall system behavior. In that regard, differences between the Mark-BW reload fuel assemblies and the resident Westinghouse OFA assemblies should not materially affect the bounding SBLOCA sequences of the reference FSARs. The BWFC and Westinghouse



assemblies differ in the following areas: unrecoverable pressure drops across the assemblies, initial fuel temperatures, initial pin internal gas pressure, and the axial power profile. The impact of each of these items, with respect to the controlling aspects of the SBLOCA transient, will be evaluated in the following paragraphs.

Mark-BW fuel assemblies have unrecoverable pressure drops that are approximately 1 psi lower than those of the Westinghouse OFA assemblies. The associated effect in overall loop pressure drop would translate to less than 1 percent difference in the initial forced flow. At the same steady-state core power and effectively identical loop flows, the controlling hot leg initial temperature is so essentially unaffected. The maximum hot leg temperature variation will be less than 1 F. Thus, the initial subcooled depressurization phase of the SBLOCA will be unaltered. The reactor trip signal and pump trips will occur at the same time in the transient as in the reference FCAR calculations.

The impact of the fuel bundle resistance will be even less during the pump coastdown and natural circulation phase because the flows during this phase are much reduced. Significant margins exist such that CHF will not be exceeded. All of the initial stored energy in the fuel will still be transferred to and removed by the steam generators. Therefore, core resistance variations will not change the fuel thermal transient or impact the existing evaluations.

Changes in the initial fuel temperature add or subtract overall energy from the RCS. The initial fuel energy is removed from the fuel pin during the reactor coolant pump coastdown phase and rejected from the system via the steam generators. Therefore, the initial fuel enthalpy of operation has virtually no impact

beyond the loop coastdown period. The core energy content during the loop draining and boil-off mode will be identical to the current licensing base.

The fuel pin internal gas fill pressures are similar to the Westinghouse values, but may differ slightly. The internal gas pressure could affect the fuel/cladding gap dimensions and rupture time. During the initial phase of the accident however, the fuel temperatures approach the system saturation temperature within a fraction of a minute following reactor trip and the impact of gap differences is negligible. During the core boil down phase the timing of rupture could differ slightly. The Mark-BW fuel pin has a larger internal gas volume, a slightly larger fuel volume, and a slightly higher fill gas pressure than the OFA. Because of the higher fill gas pressure the Mark-BW fuel will have a slightly higher internal pressure at beginning-of-life conditions. However, because of the larger gas volume available the Mark-BW pressurization with burnup will be slower than the OFA's. At burnups for which a rupture is possible during SBLOCA, the OFA fuel pin is higher in pressure than the Mark-BW. The difference, although very small, would tend to delay the rupture of the Mark-BW over the OFA. However, since the SBLOCA temperatures peak at approximately 1500 F, the impact of a difference in rupture timing on the resultant peak cladding temperature is negligible.

As a final point, SBLOCA imposed plant operating limits, including maximum allowable total peaking, will not be altered due to the use of BWFC-supplied fuel. Thus, the axial power profile used by Westinghouse in the SBLOCA analyses remains bounding. This assures that the thermal load imposed on the fuel during a temperature excursion remains conservatively modeled. The thermal results, cladding temperatures, for the present FSAR evaluations are, therefore, conservative for Mark-BW fuel.

In summary, the core resistance variations will not affect the loop flows such that the controlling hot leg temperature or CHF points are altered. The steam generator heat removal rate during the flow coastdown period will compensate for any initial fuel stored energy fluctuations. All controlling parameters in the phases following the pump coastdown and natural circulation phase will be unchanged. Therefore, since the overall RCS geometry, initial operating conditions, licensed power, and governing phenomena are effectively unchanged, the existing FSAR calculations should remain bounding for operation of the Catawba and McGuire units with BWFC-supplied fuel.

### 12.3 Current FSAR Results

The Westinghouse calculations of SBLOCA accidents for the McGuire and Catawba units are not the limiting LOCAs as predicted by the NOTRUMP and LOCTA-IV computer codes. The calculated results documented in the current McGuire and Catawba FSARs predict peak SBLOCA cladding temperatures less than 1500 F. All parameters are well within the acceptance criteria limits of 10 CFR 50.46. Even wide variations in SBLOCA results would not cause the SBLOCA to be limiting. Thus, considerable margins exist such that variations in the SBLOCA results would not alter either the plant technical specifications or operating procedures.

### 12.4 Compliance with Acceptance Criteria

The existing SBLOCA calculations contained in the McGuire and Catawba FSARs are valid and bounding for the BWFC Mark-BW fuel. The reactor coolant system, decay heat levels, and other system controlling parameters remain unchanged by the reload fuel. A significant safety margin exists between the calculated results and 10 CFR 50.46 limits. The fuel design differences between the Westinghouse OFA and the BWFC Mark-BW do not substantially alter

the results of SBLOCA evaluations. Adequate core cooling has already been demonstrated and does not need to be repeated because of the change in fuel design. The present SBLOCA evaluation calculations remain valid for the McGuire and Catawba fuel reloads supplied by BWFC. These analyses remain the small break evaluations of record for demonstrating compliance with the criteria of 10 CFR 50.46.

### 13. References

1. RSG LOCA-B&W LOCA Evaluation Model for Recirculating Steam Generator Plants, BAW-10168, B&W Fuel Company, July, 1988.
2. RELAP5/MOD2-B&W, An Advanced Program for Light Water Reactor LOCA and Non-LOCA Transient Analysis, BAW-10164P, B&W Fuel Company, October, 1988.
3. SAVER, Digital Computer Code to Determine Pressure Drops, BAW-10072-A, Babcock & Wilcox, July, 1973.
4. CRAFT2-FORTRAN Program for Digital Simulation of a Multinode Reactor Plant During Loss-of-Coolant, BAW-10092, April, 1975.
5. V. H. Ransom et al., RELAP5/MOD2 CODE MANUAL, Volumes 1 and 2, NUREG/CR-4312, EGG-2396, August 1985.
6. BEACH, Best Estimate Analysis Core Heat Transfer, BAW-10166P, B&W Fuel Company, April, 1988.
7. Mark-BW Mechanical Design Report, BAW-10172P, B&W Fuel Company, July, 1988.
8. McGuire Nuclear Plant Upgraded Safety Analysis Report, Duke Power Company, 1988 Update.
9. Catawba Nuclear Plant Upgraded Safety Analysis Report, Duke Power Company, 1988 Update.

## Appendix A. Evaluation of Transition Cores

During the period of transition from a full Westinghouse OFA core to a full Mark-BW core, the two types of fuel assemblies will reside next to each other in various mixes for several cycles of operation. This appendix addresses the LOCA and ECCS aspects of both types of fuel assemblies in the mixed core configuration. As will be shown, the Mark-BW assembly will experience slightly better blowdown cooling in a mixed core and a slightly slower reflooding rate than it does in a full Mark-BW core. Likewise the OFA assembly will experience slightly higher blowdown temperatures in a mixed core and slightly increased core flooding rates than it would in a full OFA core. In both cases, the mixed core impacts balance each other so that there is little change in expected peak cladding temperature for either fuel. Thus, the mixing of the fuel assemblies will not alter the LOCA evaluations of either fuel substantially and the evaluations of the fuels, performed as full cores, remain valid for the mixed core condition.

### A.1 OFA and Mark-BW Design Differences

Table A-1 presents a comparison of the main design differences between the OFA and the Mark-BW fuel assemblies. Differences between the assemblies occur in the pin dimensions, the guide tube dimensions, and the irrecoverable pressure drop across the assembly.

#### Guide Tube and Instrument Tube Differences

These differences do not affect a large portion of the fuel assembly and do not substantially affect the LOCA evaluation. Depending on core elevation, the flow area of the guide tube and instrument tube channels is larger for the Mark-BW than for the OFA. The shift in flow area from inside the guide and instrument



tubes to the heated channels is small and inconsequential. The effect of the change has been included in the computer runs in Section A.2.

#### Fuel and Cladding Dimension Differences

The flow area of the heated channels is lower in the Mark-BW, the heat transfer area of the fuel pin is increased, the clad thickness is increased, the gap between fuel and clad is slightly increased, and the radius of the fuel pellet is increased. The length of the pellet is decreased, but this dimension is not modeled by present techniques which treat the fuel as axially uniform. The effect of these dimensional changes is not consequential. A smaller core flow area can have a beneficial effect on flooding rate if the coolant supply from the downcomer is limited. For McGuire and Catawba, however, the downcomer is nearly always full and ECCS water is spilling from the system. With the excess ECCS water available, the decrease in core flow area going from OFA to Mark-BW provides no benefit.

The difference in cladding surface area ( $310.2 \text{ ft}^2$  per fuel assembly for the MK-BW to  $298.6 \text{ ft}^2$  for the OFA) will affect the assemblies in their individual evaluations as hot assemblies but will not affect the average core calculations. The changes in heat transfer area are somewhat offset by the changes in material thicknesses. Overall, these have no substantial effect on other than the hot pin temperature calculations.

The dimensional changes combine to make the Mark-BW materials more massive than the OFA materials. At the beginning of reflooding, the Mark-BW assemblies contain about five percent more stored energy than do the OFAs. Although this would tend to reduce the flooding rate, it is too small a difference to have a significant impact. The flooding rate comparison discussed later includes this effect and shows very low impact.

### Fuel Assembly Pressure Drop

The only difference between the assemblies that can produce a meaningful and discernable change in LOCA results is the decreased assembly pressure drop of the Mark-BW. Most of the decrease in pressure drop occurs at the fuel assembly inlet nozzle where the OFA design offsets the pin ends from the top surface of the end nozzle. With no offset in the Mark-BW design, there is less turbulence and a lower pressure loss. The effect of the different pressure drop during blowdown is to divert some flow away from the OFA assemblies and towards the Mark-BW. During reflood the impact is on the whole core pressure drop which will gradually increase the flooding rate as the core makes the transition from OFA to Mark-BW assemblies.

### A.2 Assessment of Impact on Peak Cladding Temperatures

The previous section assessed the differences between the two fuel assembly designs and the immediate impact those differences would have on system evaluations. The differences in the guide tube/instrument tube and the pin dimensions, will not significantly change the LOCA responses of the plants and need not be further evaluated. The third change, the assembly pressure drop, although only slightly different, should be considered further to assure that the resultant deviation is not substantial.

To determine the impact on cladding temperature predictions from a LOCA evaluation, changes in results caused by the mixing of the assemblies must be assessed relative to a base for which results are known. Two such bases exist. The McGuire and Catawba FSARs contain evaluations of the plants when fully loaded with the Westinghouse OFA assembly; this report contains an evaluation when the plants are fully loaded with the Mark-BW assembly. The

following subsections assess the impact of the mixed core relative to these bases. The evaluation for the Mark-BW is made by determining the impact of exchanging Mark-BW assemblies in the average core for OFA assemblies on the results presented in the main body of this report. This procedure keeps a Mark-BW as the hot assembly. Likewise, the impact on the OFA assembly is assessed by exchanging OFA assemblies in the average core for Mark-BW assemblies in the FSAR analyses. This keeps an OFA as the hot assembly.

#### Mixed Core Impact on the Mark-BW Evaluations

The higher pressure drop of the OFA assemblies present in the average core will slightly increase the average core pressure drop during blowdown. This will divert some small percentage of flow out of the average channel and into the hot assembly. Studies done during the evaluation model development show that, for a change of this magnitude, the difference in cladding and fuel temperature at the end-of-blowdown will be 30 to 50 F. Thus, blowdown temperatures can be expected to decrease slightly.

During the reflood period, the OFA assemblies will cause an increase in the core flow resistance that will make it harder for water to flow into the core. This will produce a slight decrease in the flooding rate of a mixed core, when compared to a full Mark-BW core, and a resultant increase in cladding temperature. A direct assessment was made using REFLOD3B to model a complete OFA core. The modeling included the appropriate guide tube, instrument tube, pin dimensions, and pressure drop changes. The decrease in flooding rate for a complete change of core was less than 2 percent. As expected, the core inlet mass flow increased and the core inventory increased by 2 to 5 percent. The decreased flooding rate will tend to cause an increase in

cladding temperature on the same order of magnitude as the decrease in temperature caused by the flow diversion, that is, an increase of about 30 F.

There is virtually no difference in cladding temperature during a LOCA for a Mark-BW assembly in a mixed core when compared to a Mark-BW in a pure core. The impact is certainly within the 50 F reportability measure used in the most recent revision of 10 CFR 50.46. Therefore, the calculations performed in this report, which have 200 to 300 F margins with respect to the limits of 10 CFR 50.46, can be applied to the licensing of the Mark-BW during mixed core operation.

#### Mixed Core Impact on the OFA Evaluations

To assess the impact of a mixed core on the OFA assembly, the FSAR results are used as a baseline. During blowdown, the introduction of Mark-BW assemblies into the average core with their lower pressure drop will cause some small amount of flow diversion out of the hot assembly and into the average core. From the BWFC studies this can be expected to cause a 30 to 50 F increase in the hot channel fuel and cladding temperatures at end-of-blowdown.

During reflooding, the replacement of OFA assemblies with the Mark-BW assemblies will decrease the core pressure drop and increase the flooding rate by less than 2 percent. The baseline here is a calculation of the flooding rate performed with a full OFA core. This increase in flooding rate will allow a small decrease in peak cladding temperature, estimated to be 30 F.

Thus, the difference in cladding temperature during a LOCA for an OFA assembly in a mixed core when compared to an OFA in a full core is also marginal. The impact is, again, well within the current reporting criteria of 10 CFR 50.46. Therefore, the

calculations performed in the FSAR, which have 200 to 300 F margins to the limits of 10 CFR 50.46, can be applied to the licensing of the OFA during mixed core operation.

### A.3 Conclusions

An assessment of the design differences between the OFA and the Mark-BW assemblies has concluded that the LOCA cladding temperatures for mixed core operation will not vary substantially from those calculated for the two designs in pure core operation. Furthermore, the calculations for each of the designs show considerable margin to 10 CFR 50.46 criteria. The peak cladding temperature reported for the Mark-BW in Chapter 8 of this report is 1903 F, the peak reported in the McGuire FSAR is 1841 F, and the peak in the Catawba FSAR is 1704 F. Therefore, the full core evaluations of the respective fuel assemblies can be applied for the licensing during mixed core operation. The analysis contained in the current FSAR will justify the use of the OFA assemblies, and the technical specifications applied to those assemblies will be based on those analyses. The analysis presented in the main body of this report will be applied to the licensing of the Mark-BW during the mixed core period, and the technical specifications applied to the Mark-BW assemblies can be based on the assumptions of this analysis. Operational limits or technical specifications required by either analysis that are not directly applied to the fuel assemblies will be based on the analysis which generates the most stringent limit.

Table A-1 OFA/Mark-BW Design Differences

	MK-BW	OFA
Guide Thimbles:		
Upper Section OD/r (in)	0.482/0.016	0.474/0.016
Lower Section OD/r (in)	0.429/0.016	0.429/0.016
Instrument Tube:		
OD/r (in)	0.432/0.016	0.474/0.016
Fuel Pin:		
Pin OD (in)	0.374	0.360
Clad Thickness (in)	0.024	0.0225
Pellet OD (in)	0.3195	0.3088
Pellet Length (in)	0.400	0.507
Diametral Gap (in)	0.0065	0.0062
Pressure Drop Across Core (psi) (at full flow)	22.7	23.7



## Appendix B. Fq Increase--9.7' LOCA Limits Case

[THIS APPENDIX WAS ADDED IN ITS ENTIRETY IN REVISION 1 OF BAW-10174, DATED NOVEMBER 1990.]

The analysis of the 9.7' LOCA limits case reported in Chapter 8 was based on a total peaking factor,  $F_q$ , of 2.1, as shown in Figure 8-1. This appendix presents the results of a reanalysis of the 9.7' case at a total peak of 2.23, an increase of 0.13 from the original calculation reported in Chapter 8. The updated total peaking factor curve, which replaces that presented in Figure 8-1 as an LBLOCA limit on plant operation, is shown in Figure B-1. The total peaking burnup adjustment factor curve shown in Figure 8-2 remains unchanged and applies to the peaking factors in Figure B-1. Radial peaking was maintained at 1.55 in the reanalysis, and the revised axial power profile is presented in Figure B-2.

The results presented in this appendix are based on two methodology modifications not in the Chapter 8 work. First, the Chapter 8 analyses were based on BEACH Version 10.0. During the licensing review of BAW-10174, BWFC discovered code errors in the BEACH Version 10.0 gap heat transfer logic. The errors were corrected in BEACH Version 11.0 and reported to the NRC in the response to question number 5 on BAW-10166 (BEACH), Revision 2. (NRC has approved the use of BEACH Version 11.0 in their August 13, 1990 SER on the BEACH topical report, BAW-10166.) The impact of the code errors on Chapter 8 results was assessed in the response to question number 30 on BAW-10174, and found not to produce substantial changes in PCT. Secondly, in response to question number 2 on BAW-10168, Revision 1, BWFC revised its metal-water reaction methodology from a 1500 F to a 1000 F

threshold temperature. The impact of the change on the results reported in Chapter 8 was assessed in the response to question number 13 on BAW-10174, and was found to produce small changes in results and to maintain significant margins to 10CFR50.46 limits. The update 9.7' case described in this appendix used PEACH Version 11.0 and the upgraded metal-water reaction methodology.

Figures B-3 through B-6 presents the results of the 9.7' case reanalysis, and Table B-1 presents a comparison between the original and revised cases. Basic trends between the original and revised cases remain unchanged. The PCT increased 19 F from 1823 F to 1842 F. Peak local oxidation changed from 3.7 percent to 3.4 percent, while whole core oxidation increased from 0.29 percent to 0.44 percent. The decrease in the peak local oxidation percentage is a direct result of using the quench front to terminate oxidation. For the original case oxidation was terminated based on 120 percent of the core collapsed water level, whereas the revised analysis used the REFLODB predicted quench height. Comparing quench front advancement and 120 percent of the collapsed water level, on an elevation versus time plot, shows that, above a core height of about eight feet, the quench front curve predicts an earlier quench time than does the collapsed water level curve. Based on the responses to questions 13 and 30 on BAW-10174, the 4.6' LOCA limits case will still remain limiting with respect to PCT and clad oxidation percentage.

The reanalysis of the 9.7' LOCA limits case resulted in a peak clad temperature of 1842 F, and local and whole core oxidation percentages of 3.4 percent and 0.44 percent, respectively. Core geometry (Chapter 10) and long-term cooling (Chapter 11) are not impacted by the reanalysis. Thus, compliance with the five criteria of 10CFR50.46 has been demonstrated.

Table B-1 LOCA Limits Results--Updated 9.7' Case

<u>Item or Parameter</u>	9.7' Location of Peak Power	
	<u>Original</u>	<u>Updated</u>
End-of-Blowdown, s	20.8	21.0
Liquid in Reactor Vessel		
at EOB, ft <sup>3</sup>	79.0	83.7
Bottom-of-Core Recovery, s	32.	33.1
Time of Rupture, s	84.4	84.1
Ruptured Node *	15	15
PCT at Rupture Node, F	1602	1604
Oxide at Rupture Node, %	0.8	1.1
Node Adjacent to		
Rupture *	14	14
PCT of Adjacent Node, F	1823	1842
Oxide at Adjacent Node, %	3.2	3.4
Node in Adjacent		
Grid Span *	12	12
PCT of Adjacent Grid		
Span, F	1718	1700
Oxide at Adjacent		
Grid Span, %	2.0	0.6
Pin PCT Node *	14	14
Peak Local Oxidation, %	3.7	3.4
Whole Core Oxidation, %	0.29	0.44

\* Refer to Figure 4-4 for noding arrangement.

FIGURE B-1 AXIAL DEPENDENCE OF ALLOWED TOTAL PEAKING FACTOR  
LARGE BREAK LOCA MARK-BW, UPDATED 9.7 FOOT CASE

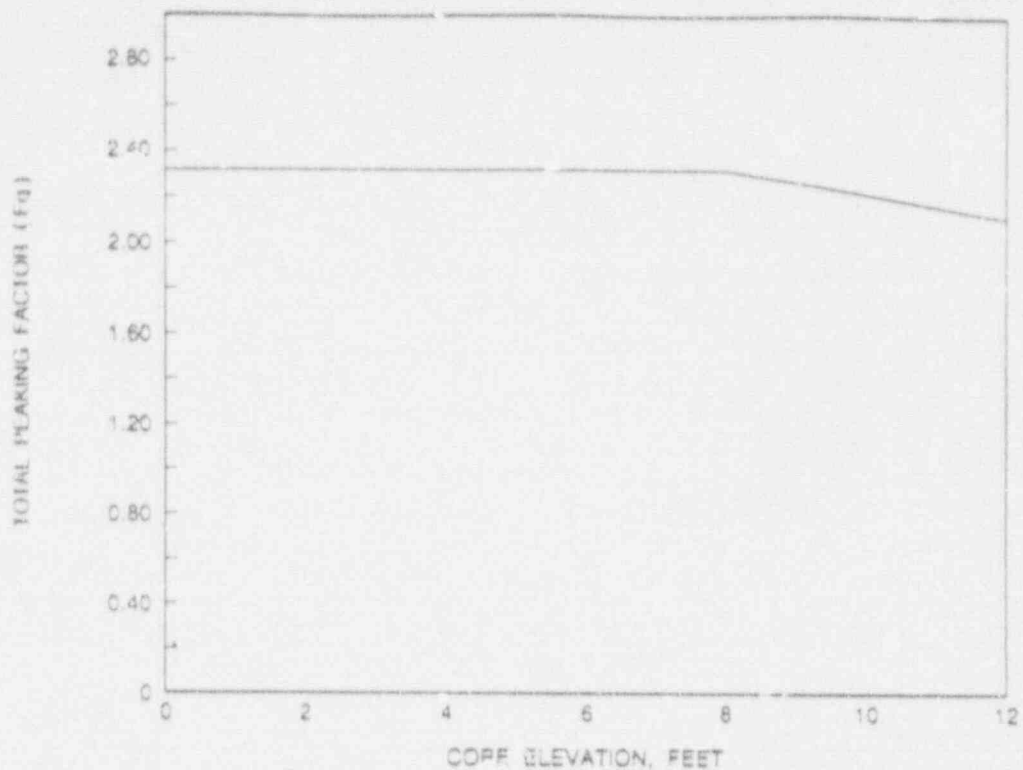


FIGURE B-2 LOCA LIMIT STUDY - AXIAL POWER SHAPE  
UPDATED 9.7 FOOT CASE

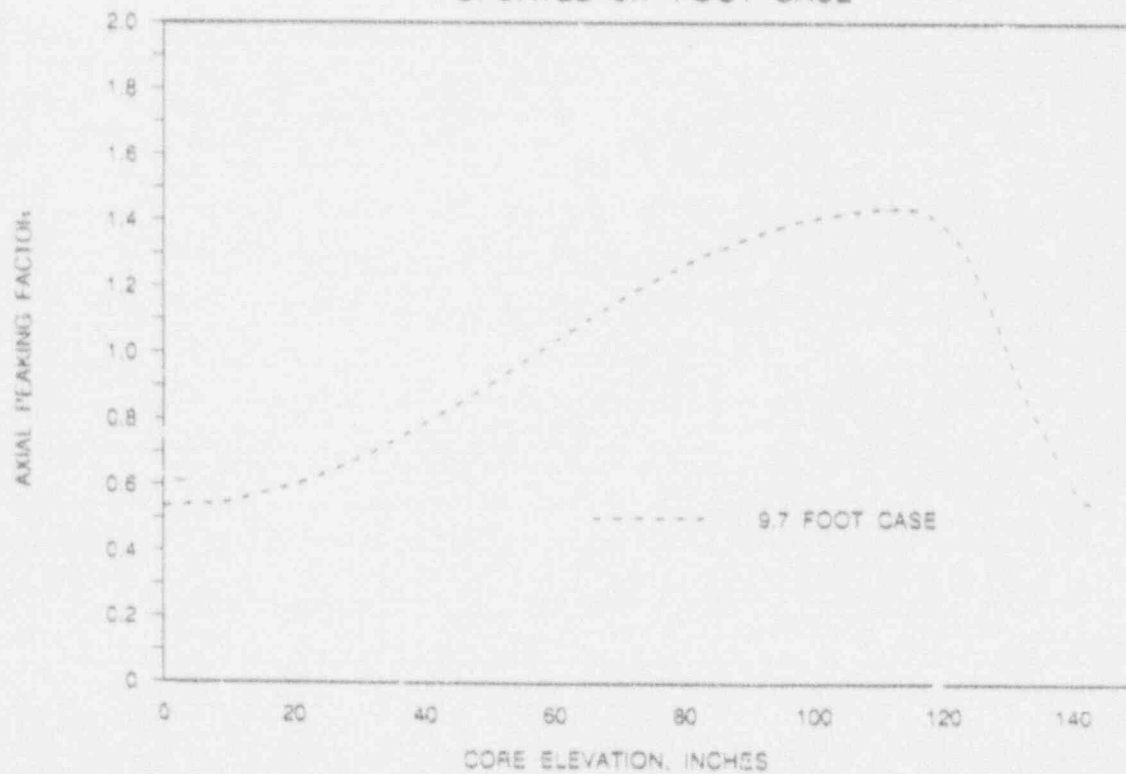


FIGURE B-3 LOCA LIMITS STUDY - UPDATED 9.7 FOOT CASE  
MASS FLUX DURING BLOWDOWN AT PEAK POWER LOCATION

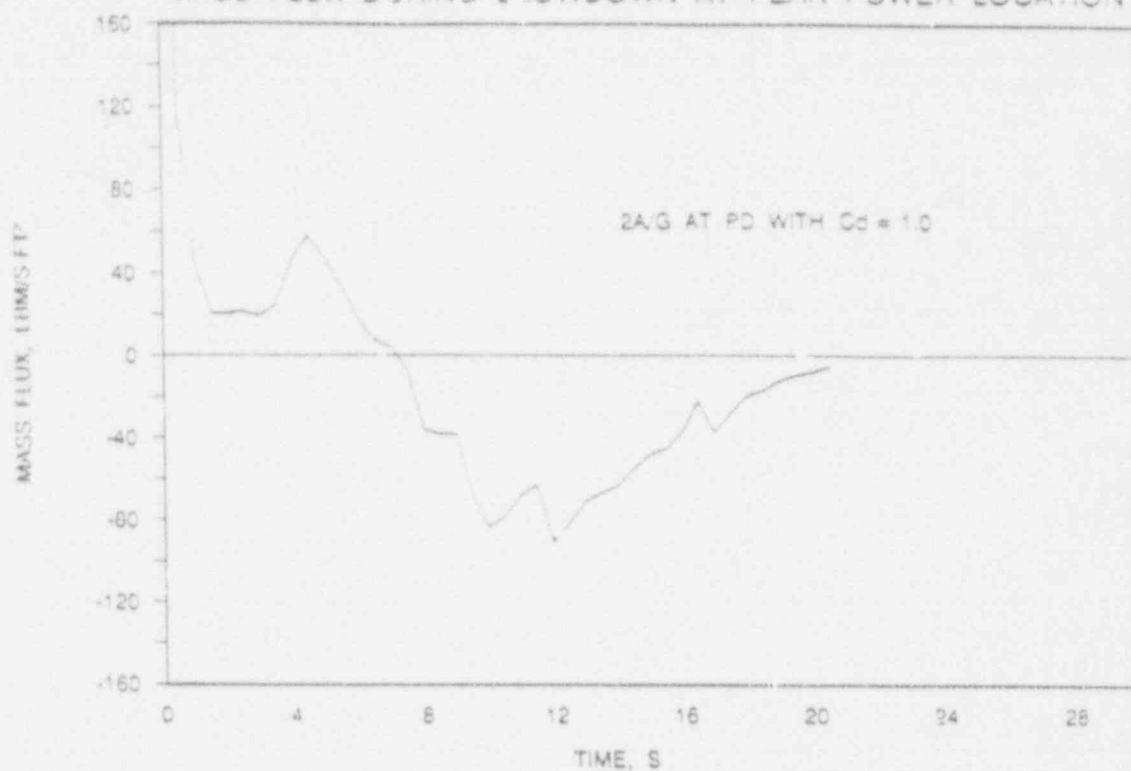


FIGURE B-4 LOCA LIMITS STUDY - UPDATED 9.7 FOOT CASE  
CLADDING TEMPERATURES

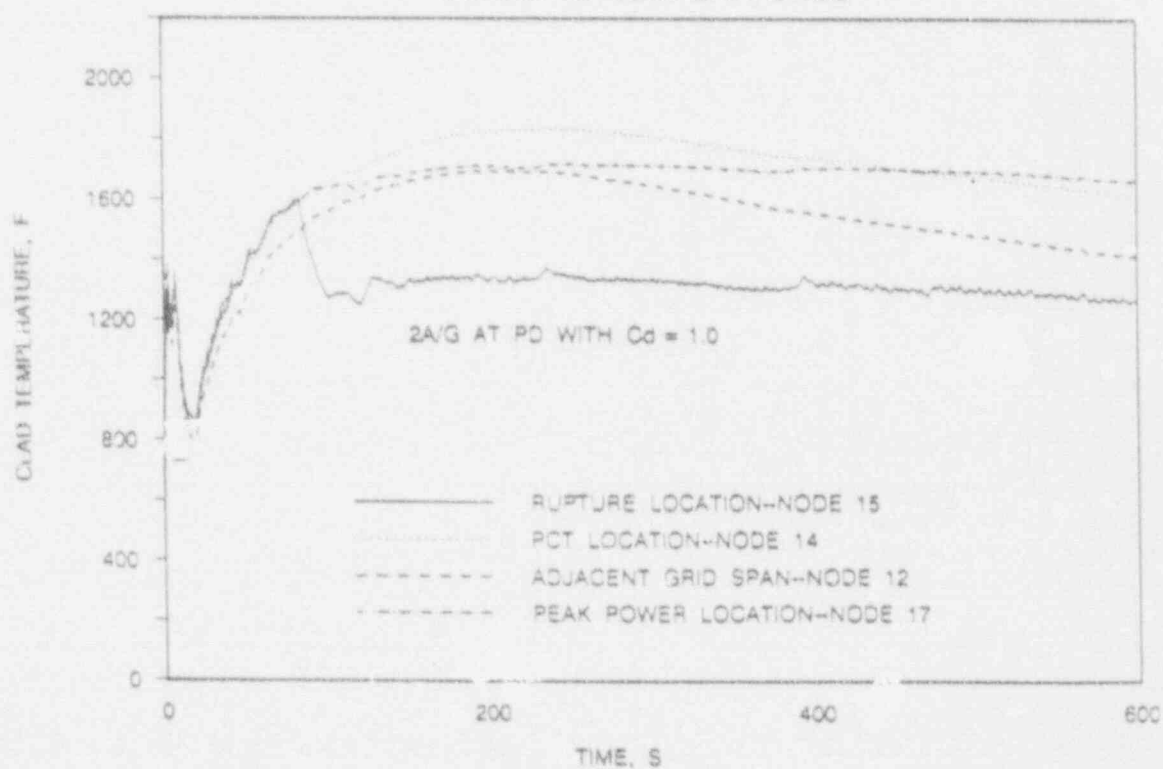


FIGURE B-5 LOCA LIMITS STUDY - UPDATED 9.7 FOOT CASE  
HEAT TRANSFER COEFFICIENT AT PCT LOCATION

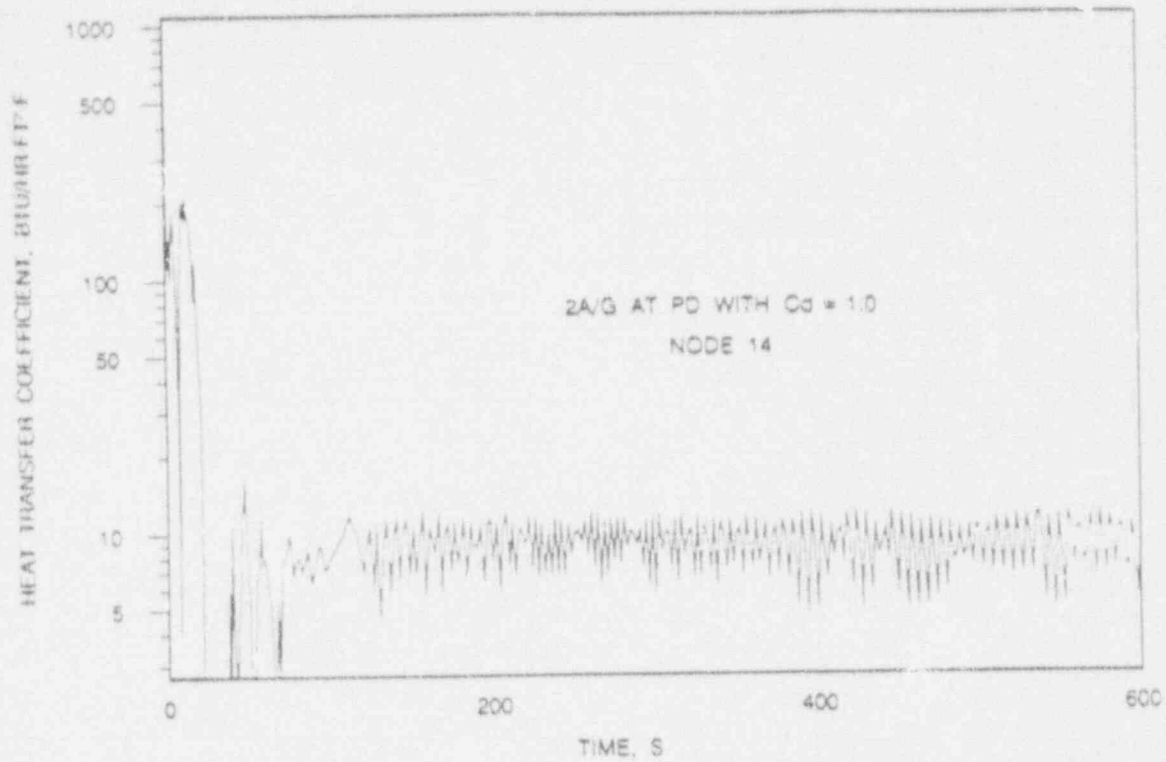
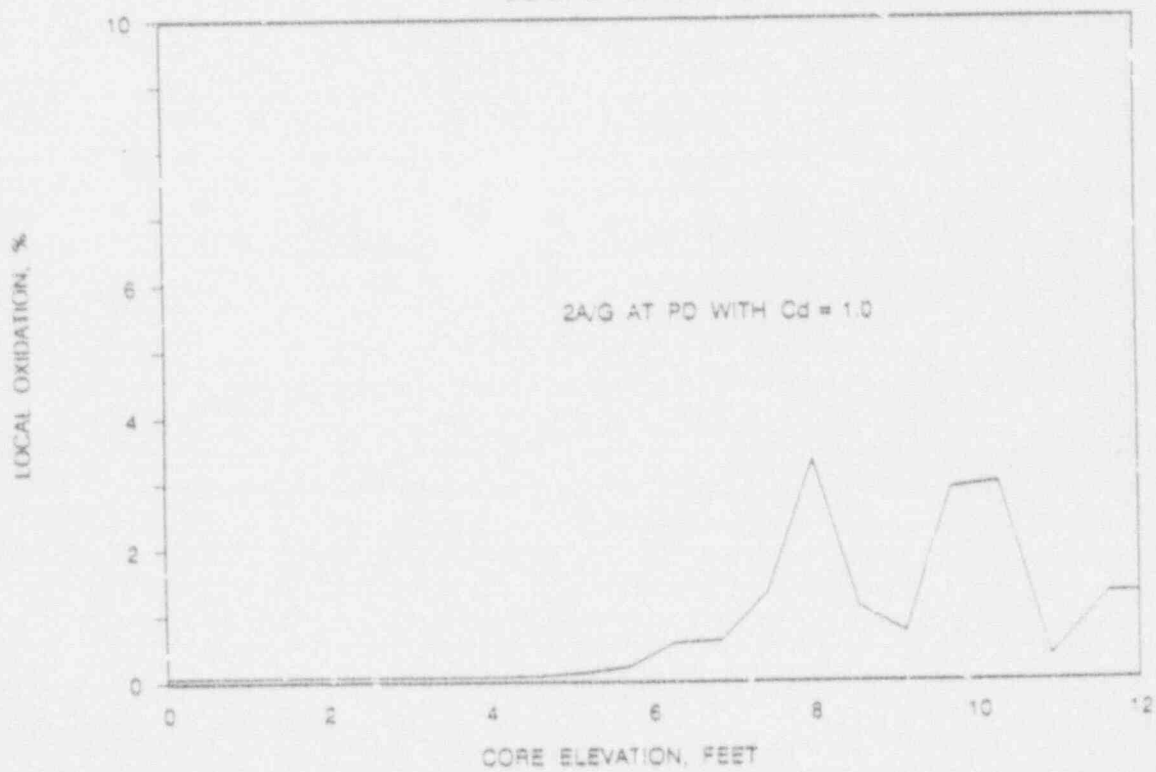


FIGURE B-6 LOCA LIMITS STUDY - UPDATED 9.7 FOOT CASE  
LOCAL OXIDATION





APPENDIX C

Responses to Request for Additional Information

1. Justify the values of fuel and cladding conductivities and gap heat transfer coefficient used in REFLOD3B.

Response: As discussed in the response to a question on the REFLOD3B code, the thermal properties of the fuel rod are selected to maximize core heat transfer, thereby resulting in lower core flooding rates. The conductivities of zirconium and helium (fuel-to-clad gap) increase with increasing temperature. For use in REFLOD3B these properties are based on an upper bound temperature of 1600 F. The average cladding and gap temperatures are considerably below 1600 F throughout the reflooding transient. Additionally, the gap coefficient is based on cold gap dimensions to maximize conductance. The resultant conductivity for zirconium is 13.698 BTU/ft-hr-F and the gap conductance, based on the cold gap thickness of 3.25 mils, is 827 BTU/ft<sup>2</sup>-hr-F.

The thermal conductivity of UO<sub>2</sub> decreases with increasing temperature. Instead of using an upper bound temperature, a fuel average temperature of 800 F is used to select the fuel conductivity. The fuel average temperature during most of the reflood transient remains above 800 F, falling below 800 F only when a majority of the core is quenched. The resultant fuel conductivity is 2.682 BTU/ft-hr-F. To evaluate the effect of fuel conductivity on core flooding rate, a REFLOD3B calculation was made with a fuel conductivity of 3.6792 BTU/ft-hr-F (400 F). The impact on core flooding rate was less than 1%. Because there is no strong sensitivity to basing the fuel conductivity on a lower temperature, the use of a temperature which is below (conservative direction) the time averaged value is sufficient for REFLOD3B calculations.

2. What were the values of Doppler and moderator void and temperature coefficients used in the analyses? Demonstrate that the values used in the analysis for moderator void and temperature coefficients were conservative values. Clarify how the reactivity versus fuel temperature and moderator density and temperature curves were calculated and used in the analysis.

Response: The Doppler temperature coefficient is -2.2 pcm/F. The moderator density reactivity curve is shown in Table 2-1. The moderator reactivity effect is dominated by the density variation and the moderator temperature effect on reactivity is not used.

For reactor kinetics calculations, RELAP5 calculates average fuel temperatures and fluid conditions for each node modelled within the active core and uses those conditions to obtain reactivities for each node. The nodal reactivities are then combined by flux-squared weighting and volume weighting to obtain a single value for use in the point kinetics equation. The reactivity coefficients, Doppler and moderator, used in the LOCA analysis have been selected as a bounding set for the burnup at which the impact of reactivity on the LOCA calculations produces the highest peak cladding temperature. Since the reactivity change due to void formation in the core dominates reactivity change due to changes in fuel temperature (for example  $\Delta\rho_{\text{mod den}} \approx -0.20 \Delta k/k$  at a relative moderator density of 0.2 while  $\Delta\rho_{\text{fuel temp}} \approx +0.013 \Delta k/k$  for a drop of 600 F in fuel temperature), the selected burnup is beginning-of-life (BOC), which gives the least negative moderator density reactivities.

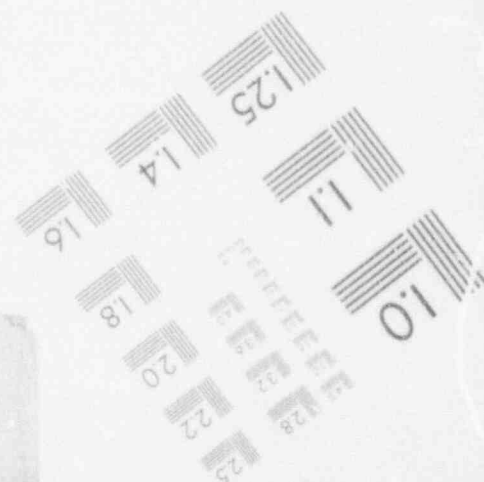
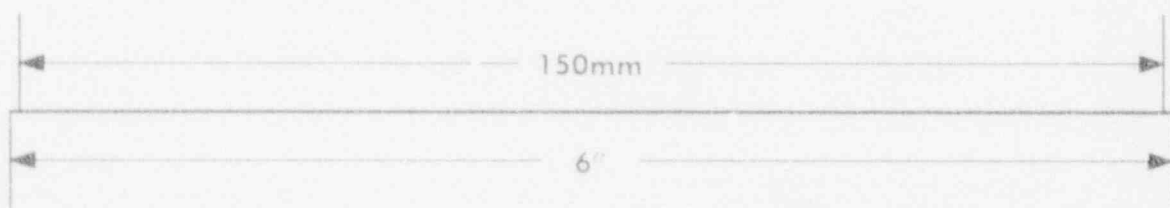
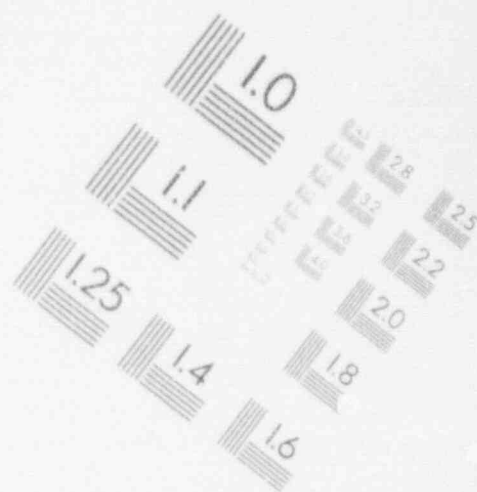
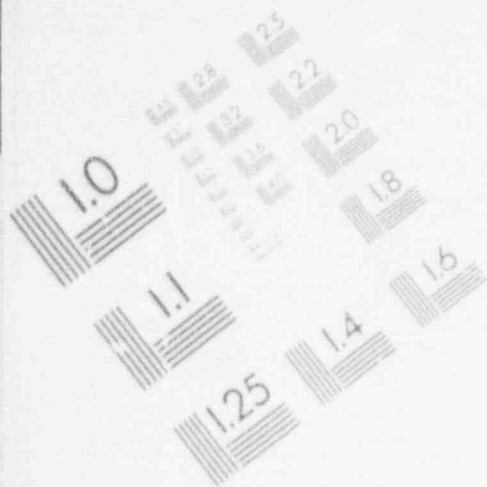
The moderator density reactivity is based on the most positive moderator coefficient allowed by the technical specifications. This occurs at BOC conditions. Technical specifications limit the moderator temperature coefficient to 0 pcm/F, and that value is verified on a cycle-by-cycle basis. The moderator density reactivity was generated using an infinite array of

assemblies with the NULIF (Reference 2.1) code. The boron concentration was varied until the moderator temperature coefficient was 0.0 pcm/F for several different fuel enrichments and burnups. The density was varied to obtain the reactivity dependence. The different fuel enrichments and burnups had a small effect upon the density reactivity curve when the fuel configuration had a zero moderator temperature coefficient. The least negative values were used. These values are conservative because radial leakage was ignored. With lower moderator densities, the mean free path of neutrons increases and the probability of escape from the core is higher, resulting in a more negative reactivity with lower densities.

Since fuel temperatures decrease during blowdown, the most negative value for the Doppler coefficient is conservative. The coefficient of -2.2 pcm/F is a most negative bounding value for beginning-of-cycle (BOC) conditions. Several possible fuel cycles were analyzed to determine the range of variation of the Doppler coefficient at BOC, with the result that the most negative coefficient was greater than -1.9 pcm/F. The end-of-cycle Doppler coefficient may approach the value of -2.2 pcm/F, but the moderator density coefficient at EOC more than compensates for this variation.

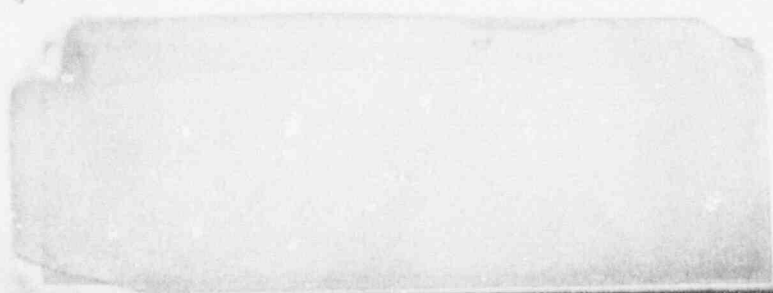
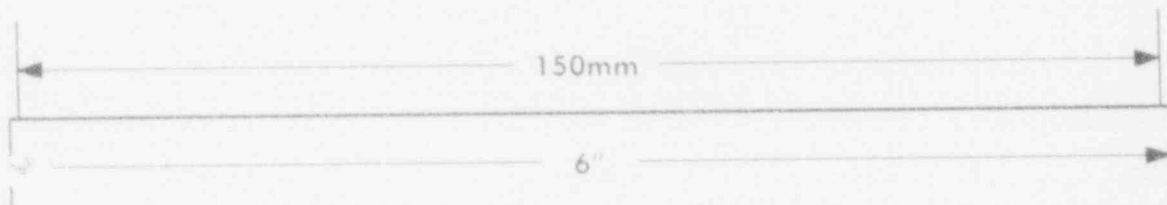
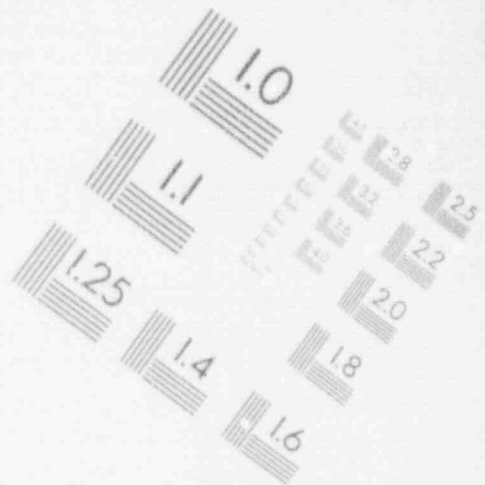
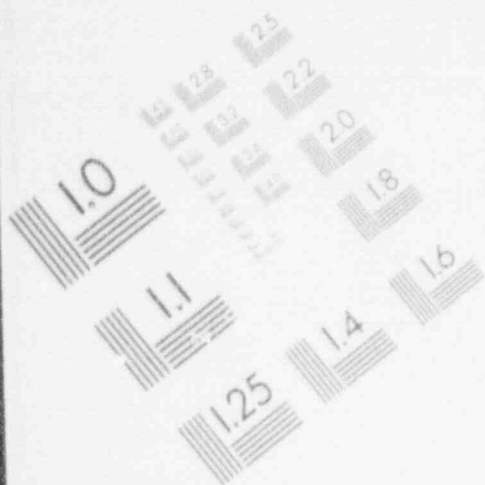
# 1

## IMAGE EVALUATION TEST TARGET (MT-3)



1

IMAGE EVALUATION  
TEST TARGET (MT-3)





1

IMAGE EVALUATION  
TEST TARGET (MT-3)

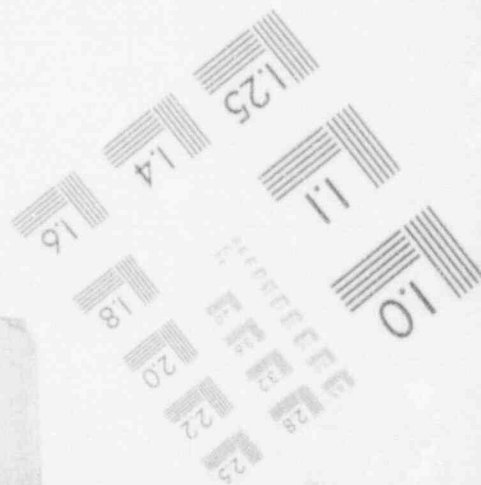
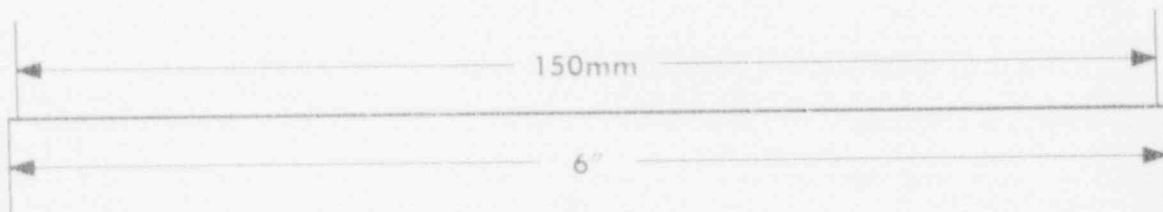
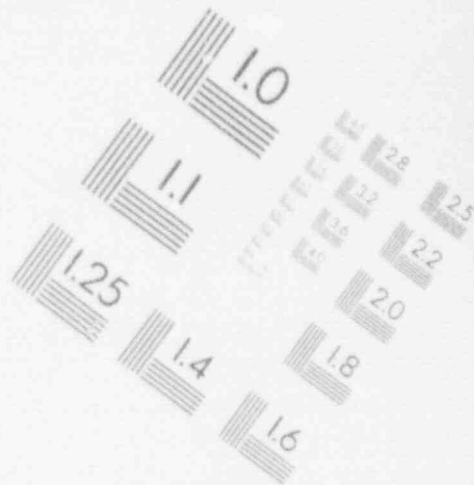
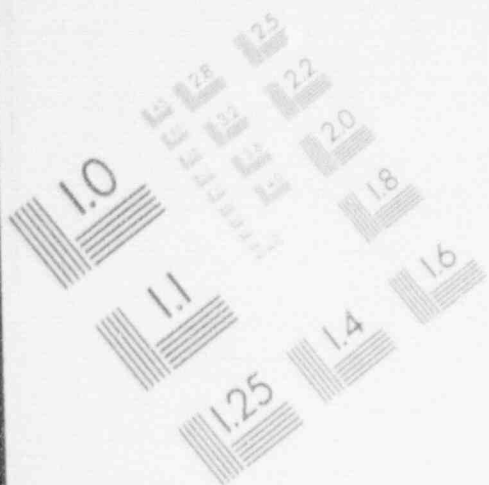


IMAGE EVALUATION  
TEST TARGET (MT-3)



150mm

6

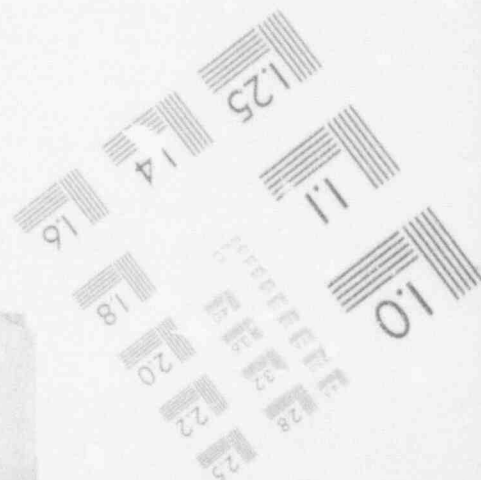


Table 2-1

Moderator Density Reactivity	
Relative Density	$\Delta k/k$
1.0	0.0
.975	-.006
.950	-.028
.900	-.122
.800	-.535
.700	-1.311
.600	-2.600
.500	-4.628
.400	-7.780
.200	-20.854

### References

- 2.1 W. A. Wittkopf, J. B. Andrews, et al., " - NULIF - Neutron Spectrum Generator, Few-Group Constant Calculator", and Fuel Depletion Code. BAW-10115, Topical Report, Babcock & Wilcox, June 1976.

3. Table 3-1 lists the system pressure as 2250 psia while page 6.9 indicates the system operating pressure to be 2280 psia. Clarify which pressure is correct and what pressure was used in the Catawba/McGuire analyses.

Response: A pressure of 2250 psia is the correct reactor coolant system operating pressure as identified in Table 3-1. The system operating pressure is quoted for the steam space of the pressurizer by convention. The pressure at the core outlet node in the RELAP5/MOD2-B&W model is approximately 2280 psia. The pressure, indicated on page 6.9, should therefore be referred to as the "core operating pressure".

4. The report stated that rod insertion was not credited until after blowdown. The Standard Review Plan requires rod insertion to be justified by an appropriate analysis before it can be credited. Provide a blowdown load and structural analysis to verify that the core and upper vessel geometry will permit rod insertion at the time credited in the analyses.

Response: As discussed in the response to Question 4 of the first round of questions on the RSG LOCA evaluation model, calculations of large breaks take credit for reactivity insertion following blowdown in order to ensure a subcritical margin during reflooding. By maintaining a core  $k_{eff}$  of approximately 0.95, the post blowdown fission power can be conservatively modelled as a constant fraction of the decay heat power. This eliminates the need for extended reactor kinetics calculations. The negative reactivities credited for this purpose have included void formation, boron injection, and control rod insertion. A recent examination of the Catawba FSAR shows that, although the fuel assembly geometry following LOCA remains amenable to control rod insertion, the upper plenum control rod guide structures may, in some locations, be displaced to the extent that rod insertion cannot be assured. Therefore, it cannot be concluded that all of the control rods will enter the core by the end of blowdown. Because of this, a revised accounting of post blowdown reactivities has been done to verify that the reflood modelling of fission power remains conservative.

Post-blowdown reactivity concerns stem from an assumption of the BWFC RSG LOCA model that post blowdown fission power can be conservatively evaluated as a constant fraction of the decay heat power. As explained in the response to Question 4 of the first set of questions, the ratio of fission power-to-decay heat is set at the end of blowdown and kept constant thereafter. Because fission power decreases more quickly than decay heat, this is a conservative treatment so long as a negative reactivity of at least 5 percent is maintained. The

contributors to core reactivity following the end of blowdown, without consideration of the control rods, are voiding above the quench front (negative reactivity), liquid below the quench front (positive reactivity), decreased fuel temperatures (positive reactivity), and increased boron concentrations (negative reactivity). Using information from Table 4.3.2-2 of the Catawba FSAR, 1987 update, the reactivity during reflood for the region below the quench front can be shown to be somewhat more negative than -0.05 without consideration of the core void content. (For zero power, cold, with no control rods  $k_{eff} = 1$  at 1650 ppm Boron -- equilibrium Xenon is worth 300 ppm Boron -- giving  $k_{eff} = 1$  at reflood conditions with 1350 ppm Boron -- because 1 ppm Boron is worth 9 pcm,  $k_{eff}$  is about 0.95 for the reflooding Boron concentration of 2000 ppm or  $\rho = -0.05$ .) Considering the effects of partial voiding below the quench front (boiling) and near complete voiding above the quench front, the net reactivity is expected to be between -0.35 and -0.05  $\Delta k/k$ . Therefore, the core reactivity during reflooding lies within the range specified in the response to Question 4 of the first set of questions and the conclusions of that response remain valid without taking credit for control rod insertion.

In summary, mechanical evaluation of the control rod guide housings in the upper plenum of the reactor vessel cannot demonstrate that all of the control rods are able to enter the reactor core following a large break LOCA. Reconsideration of the reactivities during refill and reflood, however, show that the conclusions in the response to Question 4 of the first round of questions on the RSG LOCA evaluation model remain valid even when the control rods are no longer included. Therefore, the BWFC technique for specifying the fission power during refill and reflood remains valid and there is no longer any requirement that the control rods enter the core during a large break LOCA.



5. On page 4.4, the statement was made that the upper head model in Figure 4-2b was different from the model in EAW-10168, Figure 4-3. However, the two figures referenced are the same. Clarify the meaning of the statement.

Response: Figures 4-2 and 4-3 of the RSG LOCA evaluation model topical report, BAW-10168, Revision 0, were inadvertently changed to McGuire/Catawba figures in Revision 1 of the report. These figures should have remained characteristic of a non-UHI plant because the model, used to perform the evaluations and sensitivity studies reported in BAW-10168, was for a standard 4-loop 3411 Mwt plant. Figures A-1 and A-2 of Appendix A of the evaluation model report are appropriate and correct for the report. Figure 4-2b of the McGuire/Catawba report shows an additional node in the upper plenum over Figure A-2 of the EM report. Although the UHI system has been removed from service (valved out), the upper plenum internals for McGuire/Catawba remain slightly altered from the conventional 3411 Mwt non-UHI design. The additional node allows the representation of these UHI internals structures.

Although the RELAP5/MOD2-B&W nodding diagrams in chapter 4 do not specifically apply for the report, they are not considered inappropriate as they represent a legitimate application of the evaluation model. The correct nodding is given in Figures A-1 and A-2 of Appendix A which documents most of the studies used by the report. Therefore, the figures of Chapter 4 are not substantially misleading and need not be revised immediately. They will be replaced with the original Revision 0 figures when the report is next revised.

6. On page 4.7, it is stated that appropriate time delays were used to account for delays in the activation of the emergency core cooling system (ECCS). Provide the time delays used and compare them to the actual time delays to verify the conservatism of the delays used in the analyses. Clarify if the ECC header fill time and hot wall delay were included in the delay times used in the analyses. Provide justification if they were not included. Provide the same information for other signals used in the analyses such as reactor trip, secondary isolation, etc.

Response: Time delays that effect the LOCA calculation can be considered in two general categories of system and process delays. System delays refer to the time required for actions (usually electrical or mechanical) within a system, such as the RHR system, necessary for the system to function at the level credited in the analysis. Process delays account for phenomena occurring within the reactor coolant system. The gravity delay time for ECC penetration into the lower plenum following end-of-blowdown is an example of a process delay.

#### Process Delays

Process time delays for the evaluation of LOCA are associated with the passage of water from the ECCS through the downcomer to the lower head during the refill phase of the accident. Because REFLOD3B employs only one node for the downcomer and lower plenum, the transit time through the downcomer must be accounted for independent of the code. Two effects are present that must be considered: hot wall effects and gravity fall time. The BWFC evaluation model treatment of these two items is addressed in the response to Question 1 of the first round of questions on Revision 1 of the evaluation model topical, BAW-10168.

Hot Wall Delay: This factor is intended to account for possible counter current flow limit effects associated with steam generation during the initial flooding of the downcomer walls with cold ECCS water. A representative delay, based on

small scale basically one dimensional tests, is 3 or 4 seconds during which the downcomer wall surface is being cooled and wetted to allow full penetration of ECCS fluids. The delay is only appropriate to the initial injection of ECCS into the downcomer and, once penetration starts, should not be reapplied. (In older evaluation models, developed prior to the availability of experimental data that demonstrates lower plenum penetration considerably before end-of-blowdown, the delay was applied at the beginning of the refill phase by delaying the initiation of accumulator injection in the reflood modelling by 3 to 4 seconds.)

Experimental data from full scale tests (Upper Plenum Test Facility, see the response to question 1) show that partial penetration of the ECCS water into the lower plenum starts several seconds prior to end-of-blowdown and that full ECCS penetration is occurring 1 to 2 seconds before end-of-blowdown. These data demonstrate that any hot wall effects will have taken place prior to the end-of-blowdown. Because the actual injection of ECCS is a continuous process, uninterrupted, it is not appropriate to apply such a delay at the beginning of refill modelling. The possibility that a delay might be appropriate for the accumulator injection during blowdown is accounted for within the bypass modelling which artificially bypasses all ECCS water injected prior to the end of bypass time.

Gravity Delay Time: A gravity delay time appropriate for the fall of liquid from the reactor vessel nozzles to the bottom of the reactor core is applied to the ECCS injection following end-of-bypass. The travel distance is approximately 16 feet making the delay about 1 second.

The evaluations in BAW-10174 were conducted with a procedure which set the end of bypass equal to the end of blowdown and did not impose a gravity delay time. The logic for this

approach was that ECCS water was present in the downcomer at end-of-blowdown and, therefore, there was no need to reestablish the downcomer flow column. That approach was replaced, in the response to question 1, with an alternate CCFL based definition of end-of-bypass in conjunction with the use of a gravity delay time. The calculations performed for the topical have been examined for impact with the result that a slight, about 0.5 second, shortening of the adiabatic heatup period will occur when the new model is applied. The end of bypass is about 1.5 to 1.7 seconds prior to the end of blowdown and the gravity delay time only 1 second. Leaving a net improvement of about 0.5 seconds. Thus, the calculations performed for the report are slightly conservative under the new model. There are no present plans to repeat these analyses and remove the conservatism.

#### System Delays

System time delays appropriate for the LOCA calculations are associated with the sensing, activation, and startup for the emergency injection or emergency protection systems that assist in mitigating the consequences of the LOCA. These delays are discussed on a per system basis in the following paragraphs.

Reactor Trip System: The reactor trip system acts to trip the reactor causing the insertion of control rods to shut down the nuclear reaction. Although the system is expected to function, other reactivity effects such as void formation, which occur as a natural result of the LOCA, act far faster and more effectively to shut down the reaction. Therefore, no credit is taken for the reactor trip system during the blowdown phase of the calculations, and no time delay for that systems response is necessary.

Secondary Isolation: Secondary isolation is modelled in the calculations as an instantaneous event following the calculation of system pressure below the low pressure trip set point. The effect of the steam generators in general on the results of LOCA calculations is limited. The impact of varying the time of isolation of the generators is essentially inconsequential. If anything, the instantaneous isolation is conservative as it would act to limit the cooling of the primary system by the secondary and inhibit the generation of further trip signals. Therefore, no delay time is utilized for this function.

Accumulators: The accumulators are a passive system that responds directly to the decreasing pressure of the primary system. All piping between the accumulators and the reactor coolant system is full of water during plant operation. Therefore, no time delays for operability of the accumulators are appropriate or utilized.

Pumped (High, Mid, and Low Pressure) Injection Systems: There are three pumped injection systems within the ECCS for the McGuire and Catawba plants. All three are treated with the same delay times within the calculations. The total time delay assumed from the low-low pressurizer pressure setpoint being reached until the safety injection flow at the RCS pressure boundary is credited in the analysis is 37 seconds. The following events are assumed to occur during this time period:

- o Safety injection signal is generated
- o Diesel generator starts
- o Diesel generator load sequencer actuates
- o ECCS valve motors loaded onto emergency bus
- o ECCS valves move to correct positions for injection mode
- o ECCS pump motors loaded onto emergency bus
- o ECCS pumps come up to speed and develop required head



The above sequence of events is currently tested per Technical Specification Table 3.3-5 Item 3.a and is to be completed within 27 seconds if off-site power would be unavailable. This is conservative with respect to the 37 seconds given above. The ECCS piping from the pump discharge to the RCS injection points is kept full of water per Technical Specification 4.5.2.b.1). Therefore, no ECC header fill time needs to be included in the above delay time.

Other Systems for which Time Delays are Appropriate: There are no other system used in the LOCA calculations for which time delays are considered or appropriate.



7. On page 4.3, it is stated that the liquid in the vessel at the end-of-blowdown (EOB) was transferred from the RELAP5/MOD2-B&W model and placed in the lower plenum of the REFLOD3B model. If this includes non-lower plenum liquid from RELAP5/MOD2-B&W, justify this transfer.

Response: As a result of modeling convention, the "lower plenum" nodes (REFLOD3B model nodes 3R and 4R) comprise the vessel downcomer and bottom head (below the core entrance) and represent the liquid and vapor portions of the combined vessel regions. Regions of the reactor vessel with measurable amounts of liquid at the end of blowdown include the vessel downcomer (RELAP5 model node 302), bottom head (RELAP5 model node 310), and the core bypass nodes (RELAP5 model nodes 340 through 350). Liquid inventory in the vessel at the end of blowdown which could be considered "non-lower plenum" liquid resides in the core bypass and the downcomer. Typically, the volume of liquid mass transferred from the core bypass to the lower plenum for the reflood calculation is small, on the order of 25 cubic feet. The volume transferred from the downcomer generally falls between 30 to 40 cubic feet.

The end of bypass normally precedes the end of blowdown but the two events were assumed to occur coincidentally in this calculation (further information on end-of-blowdown end-of-bypass is contained in the response to question number 6 of this set and in the response to question 1 of the first round of questions on BAW-10168, Revision 1). Any liquid left in the downcomer or the core bypass region at the end of bypass would drain to the lower plenum during adiabatic heatup. So long as the drain time does not exceed the adiabatic heatup period, the assumption of instantaneous draining will not affect the time of the start of reflood or any other process in the evaluation. Adiabatic heatup lasts between 10 and 12 seconds, while the drain period for the downcomer and the core bypass is on the order of 1 to 3 seconds.

8. The following questions are related to the containment pressure:

- a. A partial justification of the containment pressure was discussed on page 4.9. Provide a comparison of the mass and energy releases to the containment for the Catawba FSAR and B&W LOCA EM calculation cases analyzed. This comparison should include break flow and spillage from both the maximum and minimum ECCS cases. Justify that the differences between the calculations are small enough to allow the use of the Westinghouse calculated containment pressure in FSAR with the B&W LOCA EM.

Response: A comparison of the integrated mass and energy releases is provided in Figures 8-1 and 8-2, respectively. These plots show that differences in the releases (particularly the energy release) as predicted by the Catawba FSAR and Mark BW Reload analyses are generally small.

The comparisons in Figures 8-1 and 8-2 are based on the DECLG,  $C_d = 0.6$ , minimum safeguards case. Mass and energy release data are provided in the Catawba FSAR (Table 6.2.1-56) in reference to the minimum containment pressure evaluation for this case only. The information needed for a comparison of maximum safeguards cases is, therefore, not available. Nonetheless, it is not expected that the results of such a comparison would differ significantly from those of the minimum safeguards case comparison.

Three sources of effluent are considered in the M/E curves: the break flow, the bypassed accumulator fluid and the overflow of downcomer fluid that occurs during reflood. Note that overflow results when the downcomer is filled to the bottom of the cold leg and is transferred to the sump. Spillage, the flow of pumped injection and accumulator fluid delivered to the broken loop and assumed to spill directly to the containment, is not included in either the FSAR or the Mark-BW curves.

The rate of spillage is dependent on the containment pressure and would be similar for the FSAR calculations and the Mark-BW reload analysis.

The Mark-BW Reload analysis and the FSAR mass and energy releases follow the same general profile with the release computed for the Mark-BW Reload being somewhat higher. The containment pressure that would result from the Reload M/E release would exceed the FSAR curve. The higher pressure would (1) reduce resistance to the relief of steam during reflood via a reduction in steam specific volume, (2) result in a better reflood rate, and (3) effect a lower peak cladding temperature. Therefore, it is appropriate to use the FSAR containment pressure curves as boundary conditions for the reload analyses.

- b. Justify how the use of the Catawba FSAR containment pressure adequately accounts for changes to containment heat transfer surfaces and structures for both the Catawba and McGuire containments since the Catawba FSAR containment pressure calculation was completed.

Response: The containment heat transfer surfaces and structures for both the McGuire and Catawba containments were recalculated in 1987. The results of that recalculation were used in the most recent minimum containment pressure analyses given in Section 6.2.1.5 of each plant's FSAR. As can be seen by comparing McGuire FSAR Table 6.2.1-45 (1987 Update) and Catawba FSAR Table 6.2.1-59A (1988 Update), the heat transfer surfaces and structures for the two stations are very similar, to the point that the heat structure modeling for minimum containment pressure analysis is identical. The heat transfer surface and structure calculation are periodically reviewed by Duke Power Company to verify that they remain conservative with respect to subsequent plant modifications.

- c. Compare the Catawba and McGuire FSAR containment pressures to verify the lower pressure was used for the combined Catawba/McGuire analyses submitted in BAW-10174.

Response: Figures 15.6.5-41 and 15.6.5-42 of the Catawba FSAR illustrate the minimum containment pressure responses for minimum and maximum safety injection cases, respectively. Figures 15.6.4-37 and 15.6.4-39 of the McGuire FSAR illustrate the corresponding responses for McGuire (these figures are attached for convenience). By inspection, the figures show that the minimum containment pressures occurring during reflood are the pressures specific to Catawba. Using the lower pressure as a boundary condition causes lower reflood rates and higher peak cladding temperatures.

- d. Clarify if the containment pressures in Figures 7-44 and 7-47 were taken from the Catawba FSAR as discussed on Page 4-9. If not, discuss how the pressures were calculated.

Response: Figures 7-44 and 7-47 are from the minimum containment pressure curves of the Catawba FSAR. Comparison to the Duke applications report plots to Figures 15.6.5-41 and 15.6.5-42 of the Catawba FSAR (included as an attachment to the response to question 8.c.) substantiates this. The FSAR information was linearly extrapolated from the endpoint at approximately 234 seconds out to 600 seconds to provide needed boundary condition data for the reflood analysis in this period.

FIGURE 8-1  
INTEGRATED MASS RELEASE COMPARISON

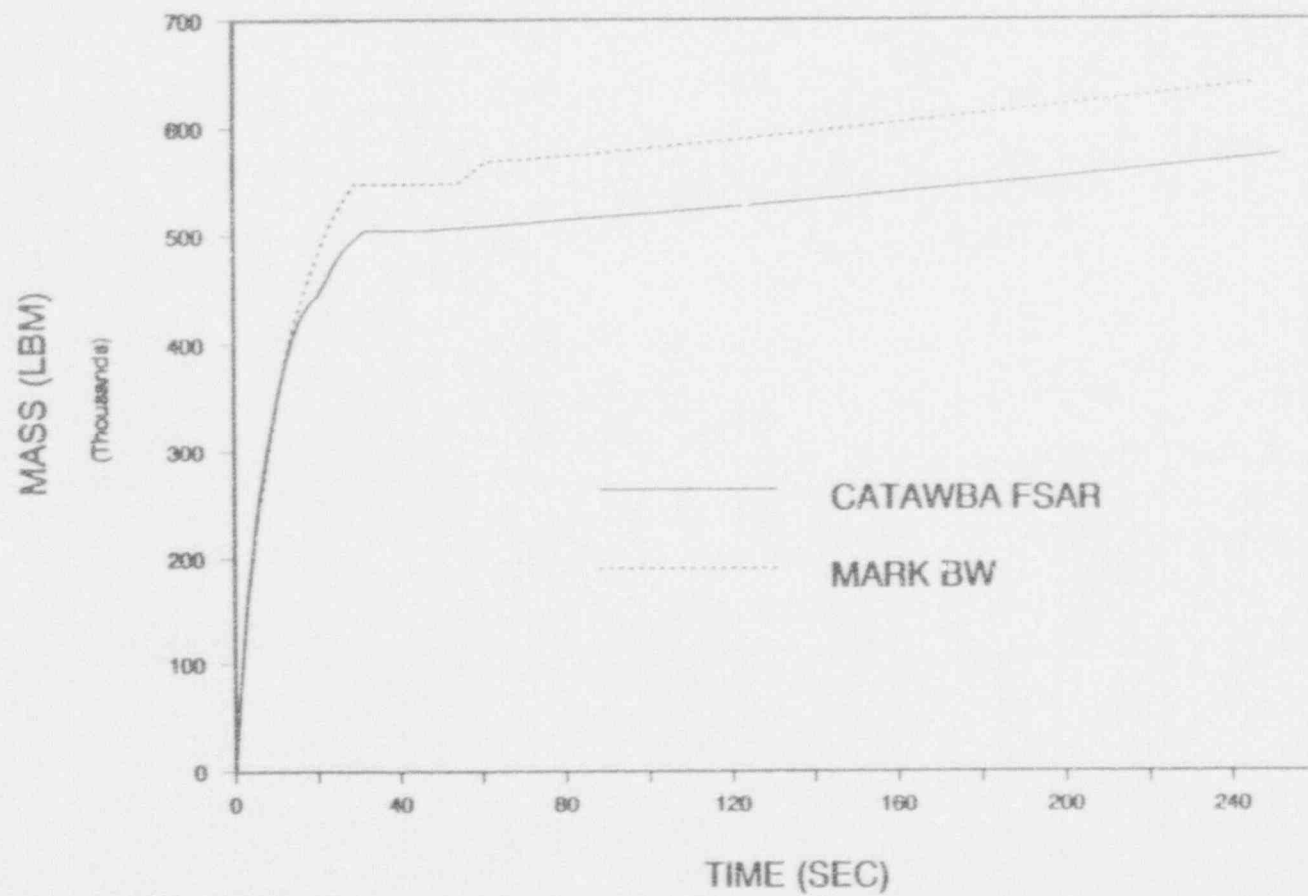
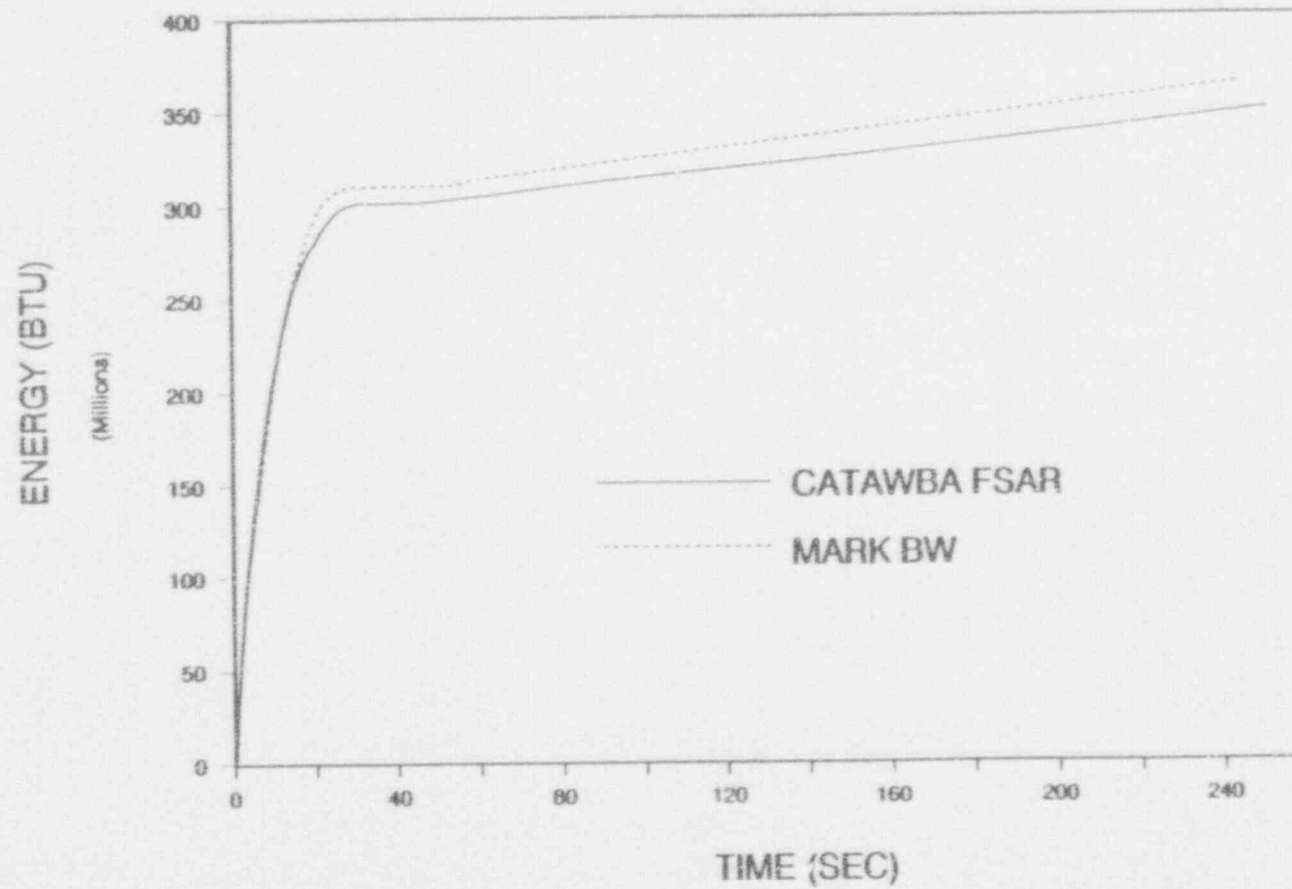


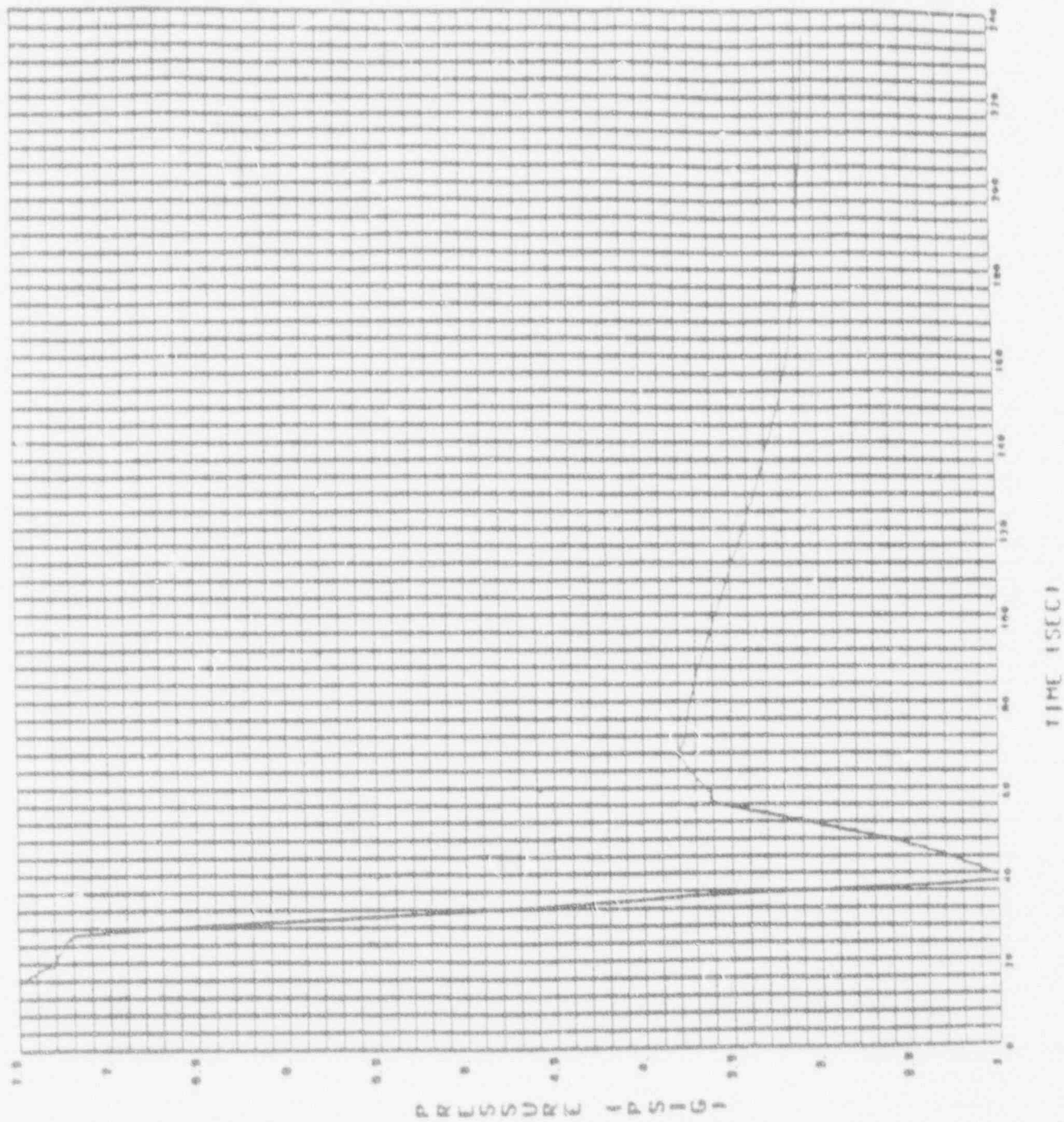


FIGURE 8-2

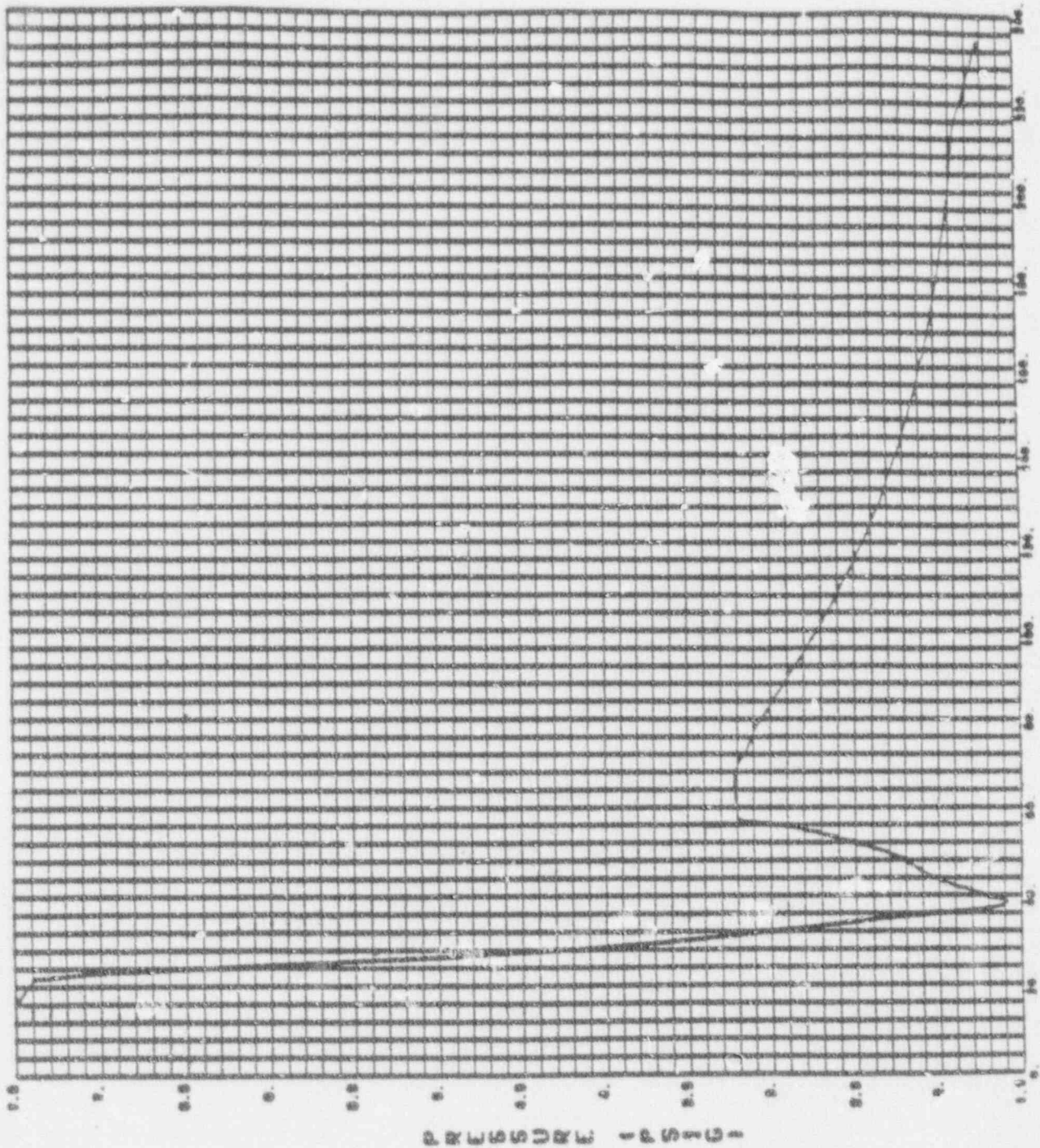
INTEGRATED ENERGY RELEASE COMPARISON



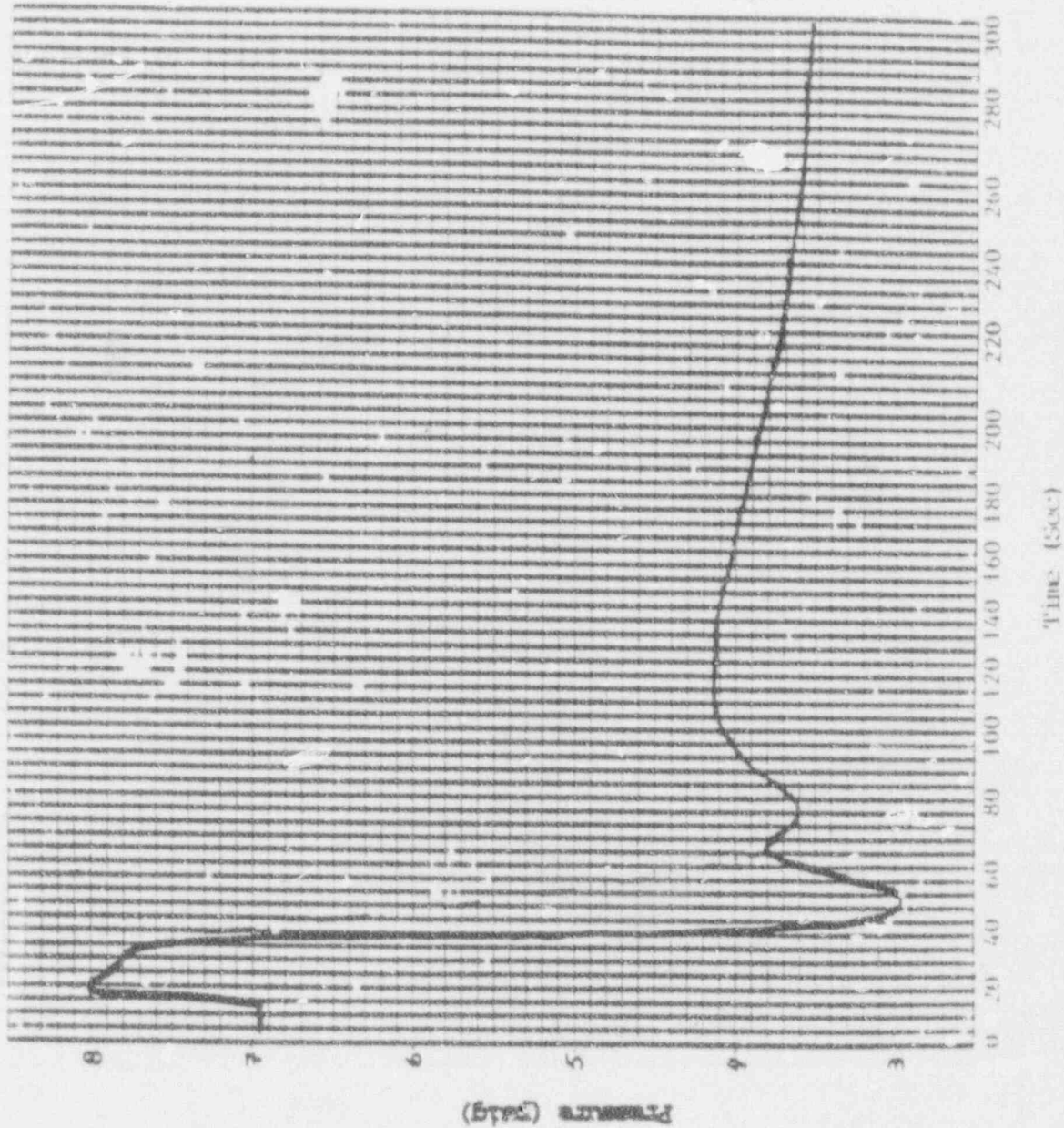




COMPARTMENT PRESSURE  
 MINIMUM SAFETY INJECTION  
 CATAWBA NUCLEAR STATION  
 FIGURE 15.6.5-41  
 1987 UPDATE

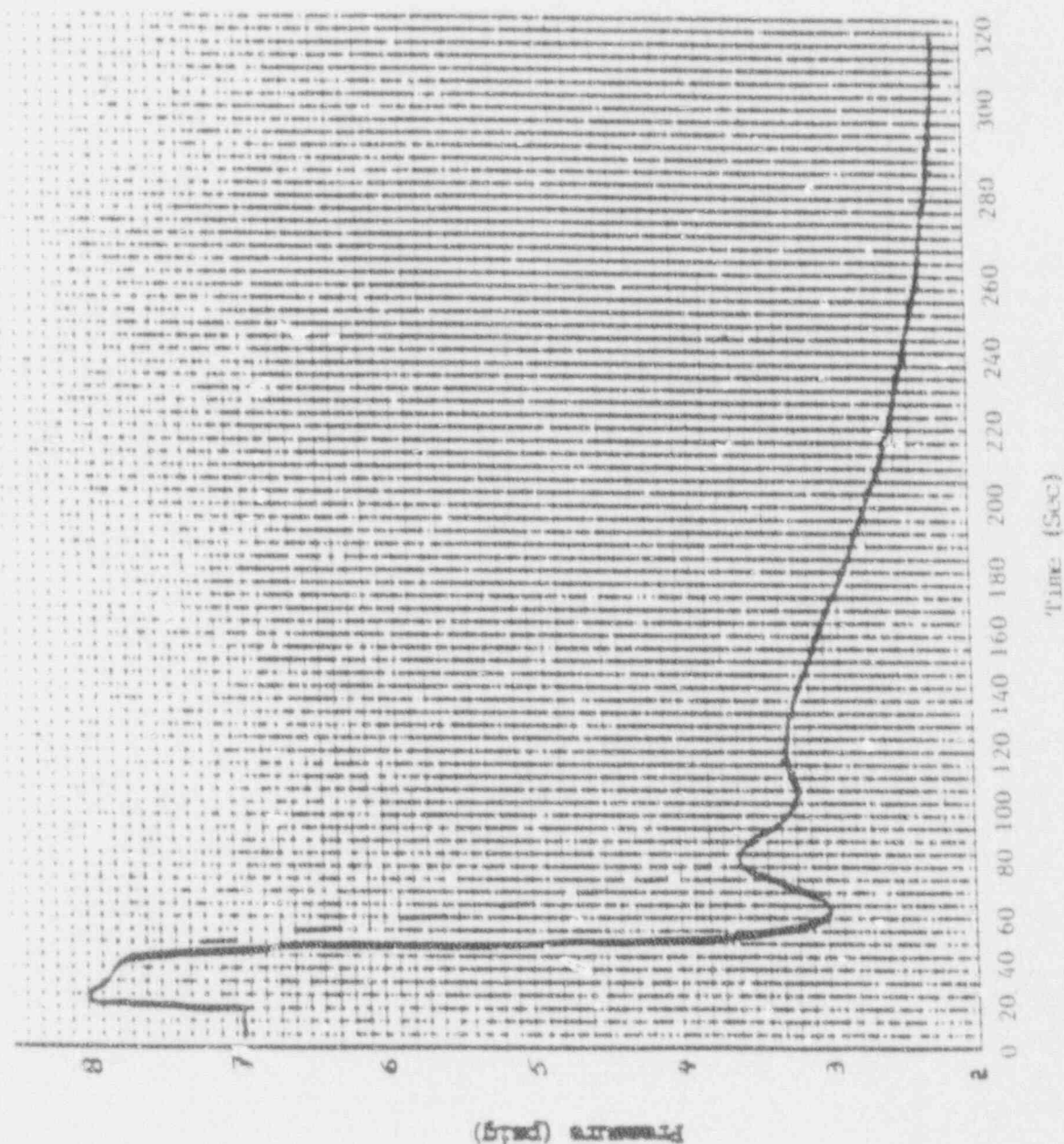


COMPARTMENT PRESSURE  
 MAXIMUM SAFETY INJECTION  
 CATAWBA NUCLEAR STATION  
 FIGURE 15.6.5-42  
 1987 UPDATE



COMPARTMENT PRESSURE,  
MIN SI  
MOGUIRE NUCLEAR STATION

Figure 15.6.4-37  
1987 Update



COMPARTMENT PRESSURE,  
MAX SI  
MCGUIRE NUCLEAR STATION  
Figure 15.6.4-39  
1987 Update



9. Section 4.3 discussed why a single generic model was chosen to evaluate Catawba and McGuire units. This section stated the differences between the two plants were not considered significant. However, the LBLOCA FSAR evaluations for the two plants showed a difference of 137°F in the peak clad temperature (PCT). Assuming the same EM was applied to both plants, Catawba and McGuire would appear to be different enough to result in this difference in PCT. Therefore, provide additional information to justify using a single model for both Catawba and McGuire or clarify why the PCT in the LBLOCA FSAR evaluations differed by 137°F and discuss how this difference is accounted for in the B&W reload analyses.

Response: As described in Section 4.3 of the topical report, a single model was used to produce bounding predictions of the LOCA transients for both the Catawba and McGuire units. The report also describes how evaluation and sensitivity studies were used to arrive at modeling assumptions and conditions that would be representative and limiting in terms of the calculated peak cladding temperatures and other applicable criteria. This process considered design and operational differences between the Catawba and McGuire units that could affect the results of the LOCA analysis, concentrating on those that could be significant.

Without access to the complete inputs and models used to perform the FSAR calculations it would be unrealistic to attempt to enumerate the differences between the Catawba and McGuire FSAR LBLOCA evaluations and produce an accounting or separate effects breakdown of the 137F difference between the two worst case peak cladding temperatures. There are noteworthy differences, however, between the two sets of FSAR analyses and between the FSAR analyses and those presented in BAW-10174P, and some discussion of these differences should clarify the inconsistency between the FSAR's and the topical report that is suggested in the question. It must be stated that the difference in results cited in the question is a reflection of the evaluation model used to produce those results and the sensitivity of that model to the various inputs and assumptions used. Any explanation offered herein

of the difference between specific FSAR values and those obtained with the BWFC codes and models must entail some speculation regarding causes and effects. To minimize speculation, the following discussion is confined to differences between the FSAR analysis that are obvious and can be readily tied to differences in the results.

Review of the FSAR analysis assumptions leads to two general observations regarding the safety injection flows used for Catawba versus those for McGuire: First, the accumulator configuration represented in the McGuire spectrum cases assumes about 10 percent less water volume than that for Catawba and flows through approximately twice the accumulator line resistance as that of the Catawba accumulator system (per the tabulation in Section 7.2 of the topical report). Thus, the safety injection flow rate from the accumulators is significantly greater for Catawba. This is consistent with the results from the study in Chapter 7 of the topical report -- namely those depicted in Figure 7-10 -- are compared against the corresponding FSAR plots, Figures 15.6.4-34, -35, and -36 for McGuire and Figures 15.6.5-38, -39, and -40 for Catawba. The second observation is that the McGuire FSAR cases assume better pumped injection performance at pressures applicable to the reflooding portion of the transient. Comparing Table 15.6.4-6 of the McGuire FSAR to Table 15.6.5-6 for Catawba shows the McGuire safety injection assumed in the large LOCA cases to be between 13 percent and 16 percent greater than that assumed for Catawba over the 14.7 psia to 34.7 psia range.

In first order terms, these differences in safety injection capacities should produce the following effects: (1) The higher accumulator capacity and lower line resistance for Catawba should result in higher accumulator injection rates. Because of the timing--mid blowdown through early reflood--the Catawba results should show earlier refill of the lower plenum



and downcomer. This is confirmed by comparing the plots designated "Reflood Mixture Levels" in the two FSARs. (2) The higher pumped injection capacity used in the McGuire cases results in a higher downcomer elevation head and produces somewhat better reflooding rates during the long term reflood period. Though the comparison is not so obvious, the "Core Inlet Velocity" plots in the FSARs confirm this effect. The sensitivity study on accumulator modeling in BAW-10174P replicates the former effect. Since the pumped injection was modeled as a lower bound for the four units, the latter effect is present, but not for the same reason.

The impact of the differences in safety injection upon the FSAR results follows reasonably from the effects described in the foregoing paragraph. That is, the shorter time to BOCREC (bottom of core recovery) for the Catawba accumulator flows results in earlier termination of the adiabatic heatup by between 3 and 4 seconds. The faster downcomer filling produces earlier reflooding and at higher inlet velocities. This can be translated to earlier turnover of the peak cladding temperature--turnover of the initial rise during reflooding--at a lower peak temperature. The Catawba PCT curve shows an additional, gradual increase during the long term reflooding period, finally peaking and turning over after 200 seconds. The McGuire PCT, given the higher long term flooding rates, peaks only once and continues to decline during the long term reflooding. (These observations are drawn from the "Large Break LOCA Sequence of Events" tables and "Peak Clad Temperature" figures in the Catawba and McGuire FSAR's.)

The differences between the Catawba and McGuire FSAR peak cladding temperature behavior, which could be tied back to the different safety injection performance assumed for each of the calculations, appear in some contrast to the BWFC results. The accumulator configuration study described in Section 7.2

of the topical report did, as previously mentioned, show the Catawba configuration to produce a shorter refill time and quicker downcomer filling, consistent with the FSAR results. In the FSAR calculation, however, the higher accumulator flow in the Catawba calculation produces an additional benefit: The downcomer elevation head is permitted to increase above the bottom of the cold leg nozzle, producing higher flooding rates. In the BWFC evaluation model, this beneficial effect is not included; the downcomer elevation head is limited at the elevation of the bottom of the nozzle. Moreover, because the BWFC evaluation model assumes complete condensation of steam on the safety injection flow, the additional accumulator flow--after the downcomer is filled--in effect represents a penalty and not a benefit as in the FSAR analysis.

The purpose of this discussion has been to describe significant differences between the assumptions used in the FSAR calculations that could produce the difference in peak cladding temperatures that is pointed out in the question. There are, to be sure, other differences between the FSAR calculations that reflect actual differences between the plants. The analyses presented in BAW-10174P have considered those features of the Catawba and McGuire units that could affect the consequences of a large LOCA and have used modeling and input assumptions designed to bound those consequences for both units.

10. Section 6.2 contained information on the time-in-life study. Provide Figure 6-2 with a log scale on the x-axis to verify the fuel stored energy is the greatest at a burn-up of 1 MWD/Mtu. Also, clarify why the time of maximum stored energy for the Catawba/McGuire submittal differs from the LOCA EM study. If the difference is due to using a different steady state fuel code (i.e., TACO2 versus TACO3), clarify the code differences that result in the different times of maximum stored energy.

Response: The difference in the time of maximum fuel stored energy between the Catawba/McGuire submittal (BAW-10174) and the analyses of the LOCA EM studies (BAW-10168) is caused by the switch from the TACO2 to the TACO3 code as the source of initial fuel conditions. The methodology used in TACO3 dictates that the energy content of the fuel monotonically decreases with increasing burnup for low exposure fuel. The TACO2 formulation, on the other hand, allowed the fuel energy content to increase for a short period of exposure followed by a monotonic decrease.

An expansion of Figure 6-2 will show the trend--fuel stored energy, starting at BOL, decreasing with increasing burnup--as described in Section 6.2 of BAW-10174. TACO3 will predict BOL as the time in life having the maximum fuel stored energy, since BOL is the time of maximum fuel/clad gap. Note that the fuel/clad gap decreases with increasing burnup thereby tending to lower fuel temperatures with increasing burnup. Of importance is the reason why TACO exhibits such a trend, and TACO2 does not. TACO2 stored energy predictions achieve a level of conservatism through a combination of conservative code and input modeling. Primary among these is the treatment of fuel densification. This issue has been addressed in the response to question 3 on BAW-10141 (TACO2 Fuel Pin Performance Analysis), which states in part:

Based on the TACO2 data comparison it has been identified that, using best estimate inputs, TACO2 will conservatively predict average fuel temperatures with a 95% probability at a 95%

confidence level provided that a margin of 120.6 F is provided in the analysis or added to the final output. This required margin is provided through the use of conservative input for densification and the use of a conservative densification model.

The fuel densification model in TACO2 was developed using Marlowe's equation to bound actual density measurements. Use of this model with conservative densification input produces an initial temperature rise such that the worst-time-in-life occurs at approximately 100 MWD/MTU. Actual fuel densification will occur much less rapidly such that the worst-time-in-life will occur at beginning of life (i.e., temperature will decrease monotonically from the BOL value) when best estimate densification inputs are chosen. (Note, however, that the 95/95 criterion would not be met.) Thus, the temperature rise occurring early in life is a conservatism due to the densification model and inputs used in TACO2.

TACO3, on the other hand, is a best estimate code, designed to predict BE fuel temperatures, which are maximums at BOL. TACO3 satisfies the 95/95 criterion through the application of an adjustment factor after completion of its calculations. Hence, maximum adjusted fuel temperatures also will occur at BOL. It is clear that the worst time in life for stored energy occurs at BOL, which is correctly predicted by TACO3 (BAW-10162-A), and that the TACO2 predicted peak at approximately 100 MWD/MTU is due to the conservative densification model and inputs used.

11. These questions relate to the analyses presented in Section 7.3.

- a. Clarify why blowdown cooling for the  $C_d = 0.8$  and  $0.6$  cases was better than the  $C_d = 1.0$  case.

Response: Blowdown cooling is best described with reference to the average fuel temperature response. The hot channel average fuel temperatures at the elevation of peak power are compared graphically in Figure 11-1.

It can be seen from the figure that the fuel temperatures differ significantly during the early portion ( $t < 12$  seconds) of the blowdown transient. During this period the mass fluxes through the core, illustrated in Figures 7-13, 7-21, and 7-29 for the break spectrum cases, are positive. Furthermore, both the magnitude and the duration of the positive mass fluxes are increased as the  $C_d$  is lowered. Consequently, the amount of heat transferred from the fuel is increased in inverse proportion to the  $C_d$ . Temperatures are lowest for the  $C_d = 0.6$  case through this period.

During the transition from positive to negative core mass fluxes, the heat transfer rate from the fuel rod is reduced. The effect of the stalled heat transfer rates is a flattening of the fuel temperature response profile. Note that this transitional point is delayed in time for lower  $C_d$ 's subsequent to the extended positive mass flux.

Progression into the portion of blowdown during which negative mass fluxes are predicted again increases the heat transfer rate and the reduction in fuel temperature continues. The mass flux magnitude and the duration of this negative mass flux period are similar albeit offset in time for all the break spectrum cases. During this period, the temperature response profiles are similar for



all break spectrum cases. The profiles are, however, offset in time relative to the delay in mass flux response. The end of blowdown is delayed with decreasing  $C_d$  and the fuel temperatures at the end of blowdown are successively lower as a result. End of blowdown hot channel average fuel temperatures of 1136 F, 1103 F, and 1098 F at the elevation of maximum power are predicted for the  $C_d = 1.0$ , 0.8, and 0.6 cases, respectively. The average core fuel temperatures are not as sensitive to  $C_d$ .

The differences in sequence timing described above are a result of the extended period of positive flow through the core that results from the reduced  $C_d$  cases. Positive core flow is primarily driven by the reactor coolant pumps and ceases when the intact cold legs become sufficiently two-phase to degrade RC pump performance. The occurrence of significant two-phase conditions in the intact cold legs is directly related to the depressurization of the system and thus to the break flow rates.

The effects described above provide the mechanisms for differing core flows and, ultimately, the differences in blowdown cooling predicted by the break spectrum cases.

- b. Clarify why the reflood rate increased for the  $C_d = 0.6$  case relative to the other two cases.

Response: The smaller effective break areas represented by the reduced discharge coefficients provide added resistance to break flow. During reflood the reduced  $C_d$  diverts some of the steam generated in the core, which would otherwise vent directly to the pump side break through the broken loop, through the intact loop to the vessel side break. The diverted steam flow, which must



vent through the downcomer, tends to pressurize the downcomer. This creates additional driving head for reflooding the core. Reflood is enhanced, relative to the  $C_g = 1.0$  case, in this manner for both the  $C_g = 0.6$  and  $0.8$  cases.

The increase in reflood rate is most pronounced for the  $C_g = 0.6$  case. This is particularly true for the specified time period of 50 to 90 seconds in a comparison of reflood rates. Note that core recovery is delayed significantly for the  $C_g = 0.6$  case. Reflood rates, immediately following the beginning of core recovery, increase rapidly and then slow down as the level reaches elevations of higher power. This initial, rapid reflood rate is occurring during the noted time period for the  $0.6 C_g$  case whereas the other cases have progressed to the slower reflood. The reflood rate is therefore significantly higher for the  $0.6 C_g$  case at this time.

- c. Clarify why the reflood rate for the split break case increased relative to the double-ended guillotine case.

Response: During reflood, steam generated in the core will preferentially pass through the broken loop and vent to the pump side break in the guillotine break case. For the split break case, a portion of this steam which would be lost through a guillotine break, mixes with the steam passing through the intact loop. The result is a higher steam pressure above the downcomer to provide additional driving head for reflooding the core.

- d. What caused the oscillations in the calculated fluid temperature, heat transfer coefficient, and cladding temperature at about 275 s in Figures 7-51 to 7-53? Did these oscillations affect the calculation of PCT? If so, justify the PCT was not underpredicted for this case.

Response: The oscillations referred to in the question are caused by the intermittent switching of heat transfer regimes in the BEACH logic. The small oscillations in the heat transfer coefficients observable after about 300 seconds result from switching between the transition and dispersed film boiling modes of heat transfer. The large oscillation (at approximately 265 seconds in each figure) is caused by switching from film boiling to the single-phase vapor heat transfer mode. The heat transfer correlations used in these modes and the criteria for switching from one mode to the other are described in the BEACH code topical report: BAW-10166P, Revision 2. The oscillations are most notable in the mid to upper axial portions of the core.

The smaller oscillations generally take place as follows: As the mixture level rises in the hot channel, quenching of the clad surface takes place. Transition boiling heat transfer accounts for the bulk of the heat removal at the elevation near the quench front. Transition boiling, a relatively effective mode of heat transfer, rapidly evaporates liquid both by direct cladding contact and via heating of the vapor field. The increase in void fraction associated with this process causes the logic to switch to the less efficient dispersed film boiling mode. The heat transfer rate is reduced, liquid drops once more coalesce, the cladding is quenched, and the heat transfer mode returns to transition boiling.

At the time of the large oscillation (about 265 seconds), void fractions above the mid-plane of the hot channel approach 1.0 during the switch from transition boiling. As a consequence of this, BEACH predicts a switch directly to the single-phase vapor heat transfer mode. The heat transfer rates are momentarily increased. Instead of contributing to liquid droplet vaporization as

before, however, this heat causes a significant increase in heat sink (vapor) temperature. The spike in cladding temperature results. Continuing reflood of the lower core region supplies liquid droplets which reduce vapor temperatures via mixing and inter-phase heat transfer. Droplets again coalesce and the heat transfer mode returns to transitional boiling to complete the cycle.

After a short time, the original cycle between transition and dispersed film boiling continues. Oscillations become small once more after this time. Ultimately, as reflood continues, the clad surface will be rewetted and the nucleate boiling heat transfer mechanism will dominate, discontinuing the cycle described above.

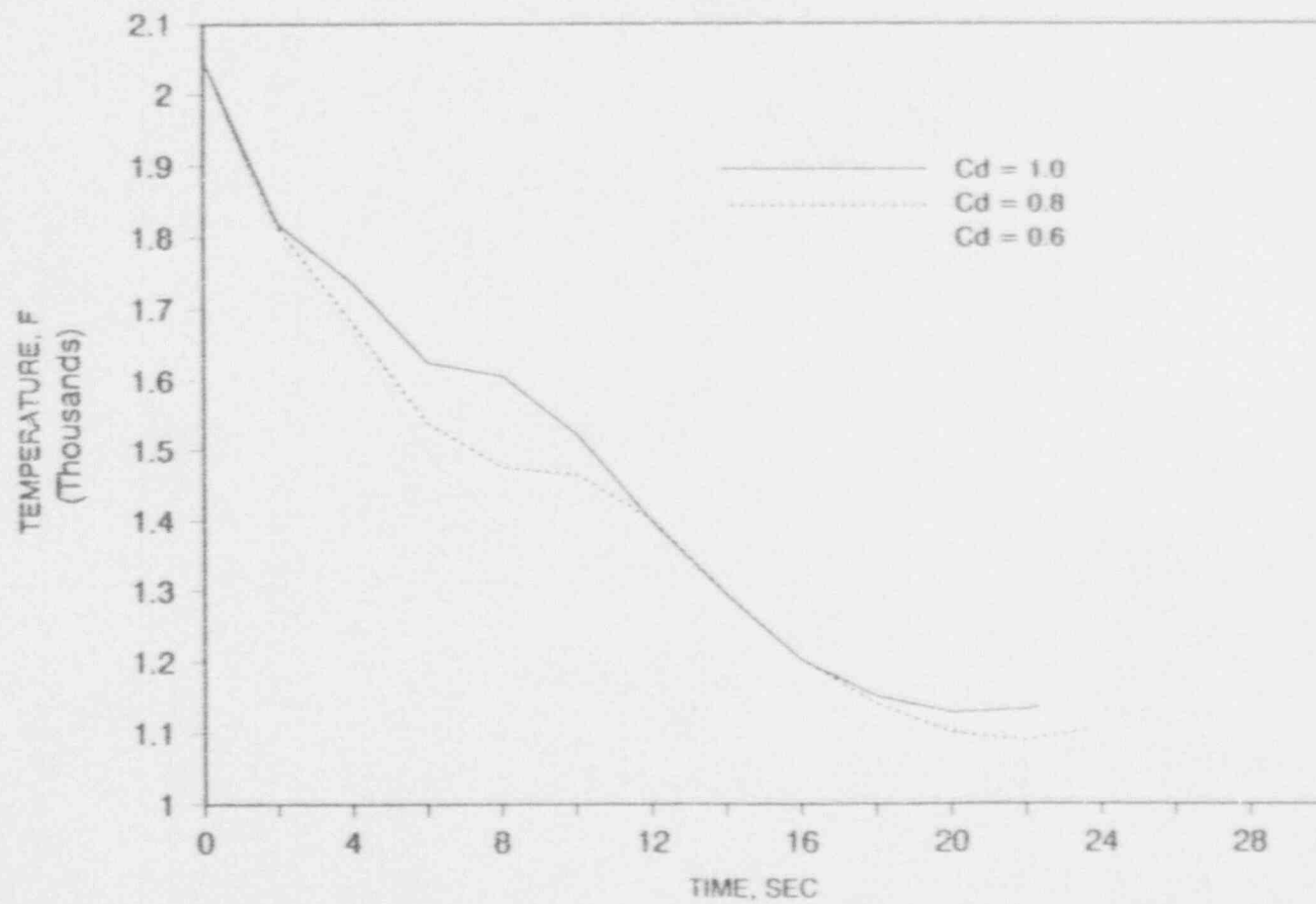
The larger oscillations occurring at about 265 seconds do affect the reported peak cladding temperatures for the maximum ECCS case. In section 7.5 it is stated that the peak cladding temperature resulting from the maximum ECCS study exceeded that predicted for the minimum ECCS case by 82 F. This figure is based on the noted spike in cladding temperature response. Ignoring the spike would result in an approximate difference of 50 F over the peak cladding temperature predicted in the minimum ECCS base case.

The purpose of section 7.5 is to show the sensitivity of the LOCA results to ECCS flow. Based on the above information, the conclusions of the section are unaffected. The maximum ECCS case predicts a higher peak cladding temperature and is, therefore, limiting. Subsequent analyses, including the LOCA limits cases, were run assuming the maximum ECCS configuration.

The oscillations in heat transfer are aphysical and not important factors in the evaluation of the peak clad

temperature. In benchmarks of the BEACH computer code to REBEKA tests, it has been shown that peak clad temperature predictions are conservative by a significant margin. This margin is very large compared with the magnitude of the oscillations and overshadows any inaccuracies that might be introduced by the existence of the oscillations.

FIGURE 11-1  
AVG FUEL TEMP, HOT CHANNEL, MAX POWER ELEVATION



12. Section 4.7 compared only the guillotine and split breaks with a discharge coefficient of 1.0. However, Appendix K requires the discharge coefficient be varied from 1.0 to 0.6 for both types of breaks. Therefore, provide the results of split break analyses with discharge coefficients of 0.8 and 0.6 to verify the worst case break type/discharge coefficient combination for Catawba and McGuire was identified.

Response: It is an established NRC practice to accept mini-spectrum analyses for licensing applications when a full spectrum has previously been performed. All PWR vendors have, as appropriate, limited their LOCA analyses for evaluation model upgrades, problem evaluations, and reloads to the mini-spectrum approach. This includes applications for core power upgrade, ECCS system change, change of steam generator tube plugging limits, alteration of reactor vessel internals configuration, and fuel assembly design change. A recent example of the application of a mini-spectrum approach is the justification for the removal of the Upper Head Injection ECCS system at McGuire. This change involved the application of a substantially different evaluation model to the McGuire plant and was performed with a three-break, mini-spectrum that did not need to reconfirm break type, break location, RC pump status, or fuel burnup. The approach to licensing of the McGuire/Catawba units for Mark-BW reloads selected by BWFC comprises somewhat more than a mini-spectrum. In addition to the mini-spectrum, break type, break location, and worst case fuel burnup have been reestablished. Additionally, the response to Question 16 of this set confirms the RC pump status. Because a full spectrum analysis has been performed for McGuire/Catawba, the mini-spectrum approach will adhere to NRC practice for demonstrating compliance with 10 CFR 50.46 since there is no reason to believe that an alternate break type will not trend with the mini-spectrum.

The identification of the double-ended guillotine as the worst break type is not unique to the BWFC evaluations for McGuire/Catawba model. Calculations performed in support of



the BWFC RSG LOCA evaluation model on the standard Westinghouse 3411 Mwt design also identified the guillotine as a worst break type. Both B&W, for B&W-designed plants, and Westinghouse, for the 4-loop plants, have shown that the split is not the limiting break type through full spectrum evaluations, references 12.1 Table 6-1, 12.2 Table 6-1, and 12.3 Table 2.3. Thus, a consistent trend showing that the guillotine is the most limiting break type has been established.

The reason that the guillotine is consistently the worst break type is related to the break modelling differences between the split and the guillotine. For the guillotine, fluid from one side of the break discharges freely through one-half of the total break area without interacting with fluid from the other side of the break. Split modelling mixes the fluids coming to the break from both sides and then discharges through the total break area. Generally, and specifically in the McGuire/Catawba analysis, there are no substantial differences in the blowdown results between the two break simulations. During core reflooding, however, the guillotine break relegates one half of the break area to the discharge of flow from the reactor vessel, whereas for a split simulation, the effective break area for reactor vessel flow is less than half the total break area. This allows more steam to vent from the downcomer for the guillotine break than for the split, reducing the downcomer pressure and thereby lowering the core reflood rate. The difference is small, resulting in only a 60 F change in cladding temperature, but observable (see Figures 7-4 and 7-38 of BAW-10174 for the results of double area breaks with discharge coefficients of 1.0). The same effect will occur at smaller break areas or reduced discharge coefficients.

The acceptability, then, of the mini-spectrum approach to licensing analyses is well established. Consistent trends,

observed in diverse calculational models, show that the guillotine is the more severe break type. This trend is evident in the calculational results for McGuire/Catawba and a causative relationship between the effect and the break modelling is present. Therefore, the peak cladding temperatures for split type breaks of areas less than the full two-area breaks or of reduced discharge coefficients will be less than the temperatures of the corresponding guillotine breaks. For the guillotine break type, the peak cladding temperature decreases with decreasing break area or discharge coefficient. Therefore, a spectrum of split breaks will not produce a cladding temperature higher than the worst case guillotine identified. A mini-spectrum of splits need not be specifically evaluated to assure that the LOCA limits are based on the worst case break. Thus, the mini-spectrum approach as conducted for McGuire and Catawba is valid and appropriate for plant licensing.

## References

- 12.1 ECCS Evaluation of B&W's 205-FA NSS - Rev. 2, BAW-10102, Babcock & Wilcox, December 1975.
- 12.2 ECCS Analysis of B&W's 177-FA Lowered-Loop NSS - Rev. 1, BAW-10103, Babcock & Wilcox, July 1977.
- 12.3 Westinghouse ECCS: Four Loop Plant (17 x 17) Sensitivity Studies, WCAP-8566, Westinghouse Electric Corp., April 1976.

13. It is stated on pages 8.4 and 9.1 that the cladding oxidation calculation was terminated when the temperature falls below 1500°F or the core mixture level covers the cladding. In your letter of March 12, 1990 (JHT/90-37) responding to NRC Question 2 on BAW-10168, Revision 1, you indicated that these criteria were being revised in the B&W LOCA EM to 1000°F or the cladding location quenched as determined by the lead quench front location. Compare the time the cladding oxidation calculation will be terminated in the Catawba/McGuire analysis according to both set of criteria and clarify what effect these changes have on the LOCA limit calculations.

Response: The maximum local and whole core oxidation percentages reported in the Catawba/McGuire LOCA applications report result from the 4.6-foot LOCA limits case. The following information relates to that particular case.

Figure 13-1 shows a comparison of the quench height to the "swelled" collapsed liquid level. The comparison gives some indication of the relative timing at which metal-water reaction should be terminated in the current and original oxidation calculations. The comparison shows that the swelled level reaches a given elevation prior to the quench front. It is implied that the revised approach would result in higher local oxidation levels. There are some differences in the approaches of the respective calculations, however, that make a direct comparison difficult.

Currently, the oxidation calculation is automated via BEACH control variable inputs that keep track of the quench height relative to the axial nodes as time progresses. The quench height is used explicitly in the evaluation of local oxidation. Prior to the March 12 letter, oxidation fractions were calculated at the end of the transient and the collapsed level was used only as rationale to determine that oxidation was complete. This procedure was determined to be overly conservative. The differences between the current and original approaches are manifested at the central elevations of the core where the oxidation is highest for this case; the

result of the current approach is lower local oxidation. Figure 13-2 illustrates the local oxidation percentages as functions of core elevation for the hot channel.

Local oxidation below the midplane and in the vicinity of the rupture elevation is lower for the current analyses as a result of lower clad temperature predictions. The lower temperatures result from correction of a BEACH code error that impacted the previous cases (see BWFC's unsolicited remarks, Question 30). The error involved the dynamic gap conductance model.

Figure 13-2 illustrates that local oxidation levels above the core midplane are higher as a result of the revised calculational approach. The differences at these elevations are explained simply by the revised cladding temperature criterion for the termination of metal-water reaction. Currently, oxidation is assumed to take place for all temperatures in excess of 1000 F. The temperatures above the core midplane tend to return to the 1000 F to 1500 F range as reflood progresses. The 1500 F criterion that was previously used resulted in less oxidation.

The whole core metal-water reaction calculation was also revised utilizing the new cutoff criterion. The results show a whole core oxidation percentage of 0.55 percent, up from the previously reported value of 0.41 percent. The increase is a consequence of the lower metal-water temperature cutoff criteria. Oxidation at elevations somewhat above core midplane increased, as described above. This increase served to lower the threshold heat rate for which significant oxidation could occur. The threshold, applied throughout the core, propagated higher oxidation not only in the upper core regions but radially as well and the whole core oxidation increased.

The revised criterion for metal-water reaction has little effect on the LOCA transient analysis. A small increase in energy release associated with metal-water reaction would normally result from the new temperature criterion because the constraint imposed by this variable is relaxed. The quench front criterion is used only for the calculation of oxidation levels. Since this is applied external to the BEACH transient evaluation, it does not impact the energy produced by the reaction, the event sequence, or the peak clad temperature.

In summary, subsequent to the application of the revised metal-water reaction criteria, respective maximum local and whole core oxidation percentages of 4.9 percent and 0.55 percent resulted for the 4.6-foot LOCA limits case. In terms of oxidation, this case is the limiting case. Since the changes in these results, relative to those reported in BAW-10174, are small and continue to indicate significant margin to 10CFR50.46 limits, further evaluation of the remaining LOCA limits cases is unnecessary.



FIGURE 13-1. COLLAPSED LEVEL - QUENCH HEIGHT COMPARISON  
FOR 4.6-FOOT LOCA LIMITS CASE.

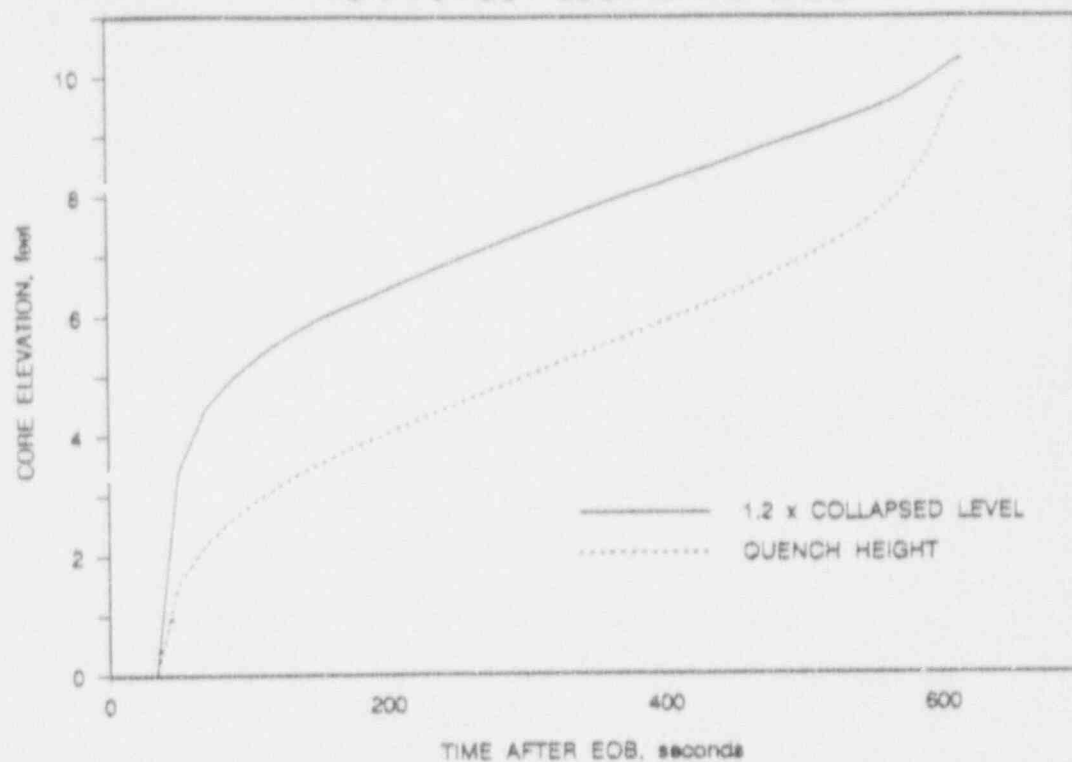
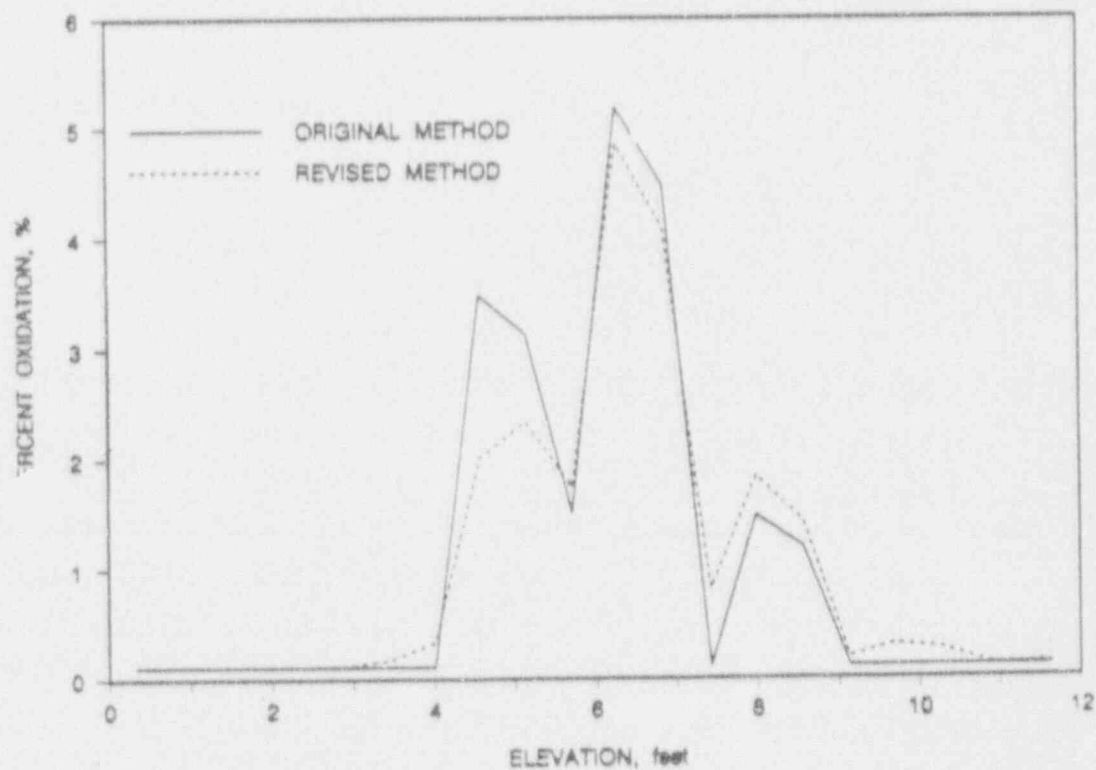


FIGURE 13-2. LOCAL OXIDATION FOR 4.6-FOOT LOCA LIMITS CASE.



14. The B&W LOCA EM provides models to enhance cooling due to grid effects and rupture. This implies the PCT calculated depends on the positioning of the axial peak and the axial power shape relative to the grids and the location of rupture. If the axial peak and power shape analyzed result in the PCT being calculated in a node with grid and/or rupture cooling effects, the PCT calculated would be lower than if the positioning of the axial peak and the power shape were such that the PCT occurred in a node without grid and/or rupture cooling effects.

Response: Nodalization of the BEACH hot channel model, as described in Appendix C of the BEACH topical report, BAW-10166, is based on the axial distribution of the important cladding cooling mechanisms across the grid span.

A pattern of three nodes per grid span is repeated up the fuel pin. Nearly all of the direct grid cooling effects are accounted for in the lowest or first node of the pattern. Physically this node models the fuel from the bottom of the grid to about 5 inches above the top of the grid and is referred to as the grid node. Droplet grid interaction and the majority of the grid-induced convective enhancement effects are modelled within this node. The second node of the three node pattern models a small amount of residual convective enhancement but primarily represents a region of the cladding near the center of the grid span that is essentially free of grid effects. The final node of the pattern models the fuel from about 7 inches below to the bottom edge of the upper bounding grid. There are no direct grid effects on heat transfer within this node. Heat removal is enhanced indirectly as a result of the grid form loss pressure drop coefficient, applied at the junction between this and the adjacent grid control volume. The pressure drop causes the node to retain entrained liquid causing an increase in interphase heat transfer and effectively reducing the vapor temperature. The effect may or may

not be substantial because it depends on the availability of entrained droplets in the flow field. During the early portion of reflooding or for upper core elevations, this cooling mechanism is minimized.

For the McGuire/Catawba calculations presented in BAW-10174, none of the peak cladding temperatures occur in nodes directly effected by grid-induced cooling mechanisms. The hottest temperatures early in the reflood period may be in either the second or the third node of a span. One of these nodes will rupture and cool while the other continues to increase in temperature and may become the location of peak cladding temperature. It is not unusual for the location of peak power to obtain the highest temperatures early in reflooding, become the ruptured location, and not be the location of the peak cladding temperature.

- a. Verify such conditions are not possible within the technical specification limits for the Catawba and McGuire plants, are bounded by the analyses already presented, or analyze the LBLOCA with this/these power shapes and peaking factors. It should be noted that, of the analyses presented in Section 8, only the 8.0 ft axial peak case did not have the PCT occur in a node influenced by grid or rupture cooling effects.

Response: As discussed in the response to Question 15, the technical specifications do not limit or control the location of peak power. The peak power will, however, be located near the center of a grid span because of grid effects on the neutron flux. The thermal neutron flux is depressed in the vicinity of the grids because of increased neutron absorption and a lowering of the local moderator content (the grid displaces water). For the grid span within which the peak power is located, the power will be lower in the grid node and pre-grid node, bottom and top nodes, than it is in the center node. The

nominal peaking in percent of total peak from the lower node  $c'$  the span to the top is 0.97, 1.00, and 0.98. (These relative values apply to the peak power grid span. For grid spans that are off-peak the overall power distribution can overshadow the grid flux depressions.)

The reference power distributions used for the LOCA limits calculation in the RSG LOCA evaluation model are simple, relatively smooth, functions intended to represent a generalized distribution and not refined to account for effects of the grids on local power. Their curvature near the location of peak power, however, is quite reasonable when compared to reality. Table 14.1 presents the nodal power distribution within the peak power grid span for the 4.6-ft, 6.3-ft and 8.0-ft LOCA limits cases in comparison to the realistic values. The power shape used in the LOCA calculations is representative of the real shape, or may be slightly flatter than can actually be expected. For the highly skewed shapes, 2.9-ft and 9.7-ft peaks, the severity of the skew of the overall shape combines with the grid neutronics effects to cause a larger depression for the node nearest the end of the core. A reference realistic distribution for these highly skewed cases has not been developed but it can be expected to verify the shapes used in the LOCA analyses.

As shown, the power distributions used for the LOCA calculations are not significantly different from the profiles that result naturally from grid flux depression. Therefore, although the power profile within a grid span is not dictated by technical specifications, the distribution used in the LOCA calculation for the high power grid span is assured. Further flattening of the distribution near the peak power location would be a small, but unwarranted, increase in conservatism.

Table 14.1 Flux Ratios				
Node	Typical	4.6' LLIM	6.3' LLIM	8.0' LLIM
Grid	0.97	0.98	0.99	1.00
Middle	1.00	1.00	1.00	1.00
Upper	0.98	0.99	0.99	0.97

- b. In Section 8.2, the highest PCT was calculated for the case with the axial peak at 4.6 ft. The PCT was calculated to occur in node 11, a node with some grid cooling effects from the grid in node 10. Could a higher PCT be calculated if the axial power shape was adjusted slightly (for example, move the axial peak from node 8 to node 9) so that the PCT was calculated to occur in node 12, a node without grid cooling effects? Other cases where this combination of PCT and grid cooling effect occurred were the 2.9 and 9.7 ft axial peak cases.

Response: The central distribution of power within the peak power grid span is discussed in the response to part (a) of this question. A shift of power peak upward from 4.6 ft, within the framework of the EM nodalization scheme, would result in shifting the peak power to another grid span, a case that is already analyzed. The power in Nodes 11 and 12, however, could be increased by an alteration of the power shape from the reference used in the LOCA calculations without repositioning the peak power.

The response to Question 15 discusses alternate power shapes and the need to evaluate them with the conclusion that the present set of power distributions used in the BWFC RSG LOCA evaluation model provide a realistic bound to the shapes that can occur at the limits of plant

operation. In reaching that conclusion, the sensitivity of peak cladding temperature to an unrealistically flat axial power profile was determined to be less than 60 F. Applying the same technique to the evaluation of a power shape that increases the power in Nodes 11 and 12 by an amount suggested in Part (b) of this question would give an increase in peak cladding temperature of about 30 F.

In summary, then, moving the peak power location from the middle node of a grid span is not supportable physically. Adjustment of the axial power distribution is not warranted because the sensitivity of cladding temperature to the shift is small and the distributions presently employed form a realistic bounding set. The current approach set out in the EM is, therefore, sufficient for the evaluation of peak cladding temperature and the setting of local power operation limits in accordance with 10CFR50.46.



15. Due to the slope of the axial shape on either side of the peak power location, the power at the PCT location was less than at the peak power location (see Figure 8-3). Clarify if a worst case axial shape could be defined within the Catawba and McGuire technical specification limits so that the axial shape was flatter in the vicinity of the axial peak and the power was higher at the PCT location. For example, in the 8.0 ft axial peak case, the axial peak was in node 14 and the PCT occurred in node 12, at 6.9 ft. Would a higher PCT have been calculated if the axial shape was flatter around the peak power location so that additional power was applied at the 6.9 ft elevation? A similar question applies to all other cases.

Response: The limit on power shapes imposed within plant technical specifications because of LOCA is restricted to specification of the peak power as a function of elevation in the core. No provision is made to specify a shape or a distribution of the total peak between the axial and radial factors. To determine power shapes with which the allowable peak power versus core position values will be set, conservative, pragmatic, and phenomenological factors are balanced. In so doing, the RSG LOCA evaluation model requires that five separate power shapes, each peaked at a different elevation in the core, be evaluated.

The basic philosophy for determining the power shapes is to use realistic power shapes with normal but high peaking and increase both the axial and radial peaking in a reasonable fashion until the absolute or the desired power limit is reached. Although operation is allowed up to and beyond the limits (the limiting is by administrative control such that being above a limit only requires a plant to take control measures to return below the limit), it is seldom if ever achieved, and there is no most probable way by which peaking would increase to the limits. Therefore, by starting with real shapes and pushing them to the limit in reasonable ways, the power shapes that result can be described as possible shapes if operation at the limiting local power levels were to occur.

While the power shapes used are representative of shapes possible for operation at the limits of local power, they are bounding for the operations of the plant because they are at the limits of allowable operation. This, in combination with the use of five separate axial distributions, provides broad coverage of the possible power shapes that can be achieved and makes the LOCA limits power distributions, as a set, essentially bounding for operation of the plant.

Because the LOCA limits shapes are derived from abstractions of actual shapes, they are curved over the entire length of the core. Within the vicinity of the peak, however, the curvature is not so severe that complete flattening would seriously alter the results achieved. To provide a measure of the effect of total local flattening of the power shapes a study was made that, using the LOCA limits cases as a base, adjusted the cladding-to-vapor temperature difference at each axial position by the ratio of the peak local power to the local power at the axial position. The results are considered credible for the grid spans above and below the span that contains the peak power in the base. Within this range the peak cladding temperatures were increased by 10 to 50 F. The highest cladding temperature within the LOCA limits set would increase by 49 F. Therefore, further flattening of the power shapes will not substantially alter the results obtained by current practice.

In summary, a set of five power shapes are utilized for the LOCA limits studies under the BWFC LOCA evaluation model. These shapes provide, as a set, a realistic bound to the possible power shapes that can occur at the limits of plant operation. Moreover, the effects of an increase in the flatness of the power shapes has been shown to be limited to a 60 F possible increase in peak cladding temperature. Therefore, the methodology of the BWFC RSG LOCA evaluation

model offers a practical yet realistically bounding approach to the determination of LOCA-induced operational limits.

16. Provide a sensitivity study on the effect of reactor coolant pump running or trip for large break LOCA.

Response: The status of the reactor coolant pumps following a large break LOCA may change the blowdown transient characteristics by altering the distribution and magnitude of core and loop flows. To evaluate the impact of the pumps-off (tripped) versus the pumps-on (not tripped) operational status, a blowdown sensitivity study was performed. The study was performed using a McGuire/Catawba model of a double-ended guillotine break at the pump discharge with the peak power at the center of the centermost grid span, 6.3 ft. Both a reactor coolant pumps-powered and a pumps-tripped (unpowered after the start of the accident) case were run.

Comparisons of the pumps-on and pumps-off cases are shown in Figures 16-1 through 16-6 and in Table 16-1. Variations in results of the two cases are apparent during the first half of the transient, but the differences are substantially reduced by the end of blowdown. System pressures and the break flows are only slightly affected, Figures 16-1 and 16-2. The higher pump head, Figure 16-3, in the unbroken loops for the pumps powered case increases the positive core flow during the first half of blowdown, Figure 16-4. The increase in core flow results in a reduction in hot channel cladding temperature by slightly more than 100 F until the core flow becomes negative, Figure 16-5. As the pumps experience two-phase flow, and performance degrades, the core flow becomes negative. At this juncture, the pumps-on core flow, because it remains more positive than the pumps-off core flow, cannot cool as well and the cladding temperatures converge. The pump status makes little difference during the high quality two-phase flow of the last half of the blowdown, has little effect on the core flow, and the cladding temperatures generally track each other during this period. At the end of the blowdown, hot rod clad and fuel temperatures at the peak power location are lower by

about 25 F in the pumps-on case. Thus, during blowdown, the continued operation of the reactor coolant pumps provides some small additional cooling for the core.

The evaluation of reflooding is not necessary because powering the pumps will clearly benefit the transient. The blowdown parameters of primary interest for the reflooding analysis are the EOB time, the EOB core stored energies (core temperatures), and the mass remaining in the vessel. As shown in Table 16-1 the differences in these parameters are extremely small, will make little difference in the calculational results, and tend toward causing higher temperatures in the pumps-off case. In section A.2.4 of the BWFC evaluation model report, BAW-10168, the locked rotor assumption is shown to produce higher cladding temperatures than does a free spinning rotor assumption. Forcing a positive rotation would enhance the free-spinning affect, causing some additional reduction in relative cladding temperatures.

FRAP-T6 has been shown to follow the trends if not the magnitudes of the RELAP5 hot channel performance. Thus, relative judgements based on RELAP5 are valid for FRAP-T6. Similarly, the BEACH analysis, which begins where the FRAP-T6 analysis ends and uses the REFLOD3 generated inlet flooding rates, would continue and augment the trends established during blowdown.

The similarity of the system responses for the two cases suggests that the pumps-off condition is acceptable as a generic condition for application to LBLOCA. Generally, the uncertainties associated with modeling pump performance parameters are greater than their impact on fuel rod temperatures. For example, the sensitivity study described herein and the study on pump degradation in Appendix A of BAW-10168 showed maximum sensitivities of well under 100 F for

very large changes in pump performance factors. It is therefore concluded that the status of the pumps, powered or unpowered, is at best a minor determinant of the calculated cladding temperature. It is concluded that plant-specific pump status sensitivity studies are unnecessary and that "pumps-off" is a reasonable configuration for the calculation of large break LOCA. The calculations presented in BAW-10174 use the "pumps-off" assumption and remain valid for licensing of the McGuire and Catawba facilities when operated with the Mark-BW fuel.



Table 16-1. EOB Summary Of Pumps-On And -Off Cases.

<u>Description</u>	<u>Value</u>			
	<u>Pumps-on</u>	<u>Pumps-off</u>	<u>Difference</u>	
EOB, s	21.26	21.08	0.18	
Temperature At Peak Power Location, F				
Hot Pin	Clad:	1032.9	1055.5	-22.6
	Fuel:*	1090.5	1115.0	-24.5
Ave Pin	Fuel:*	867.69	885.55	-17.9
RV Water Volume, ft <sup>3</sup>	91.7	87.0	4.70	
Accumulator Water Volume, ft <sup>3</sup>	2377.3	2390.3	-13.0	
Intact Loop Accumulator				
Injection, lbm	53063.7	52259.6	804.1	
Integrated Leak Flow To				
Containment Including				
ECC Bypass, lbm	527546.0	526440.9	1105.1	
Integrated Leak Energy To				
Containment Including				
ECC Bypass, BTU	3.050E+8	3.041E+8	0.01E+8	

\* = Volume Average

FIGURE 16-1. RELAP5 PUMPS ON/OFF STUDY - UPPER PLENUM PRESSURE

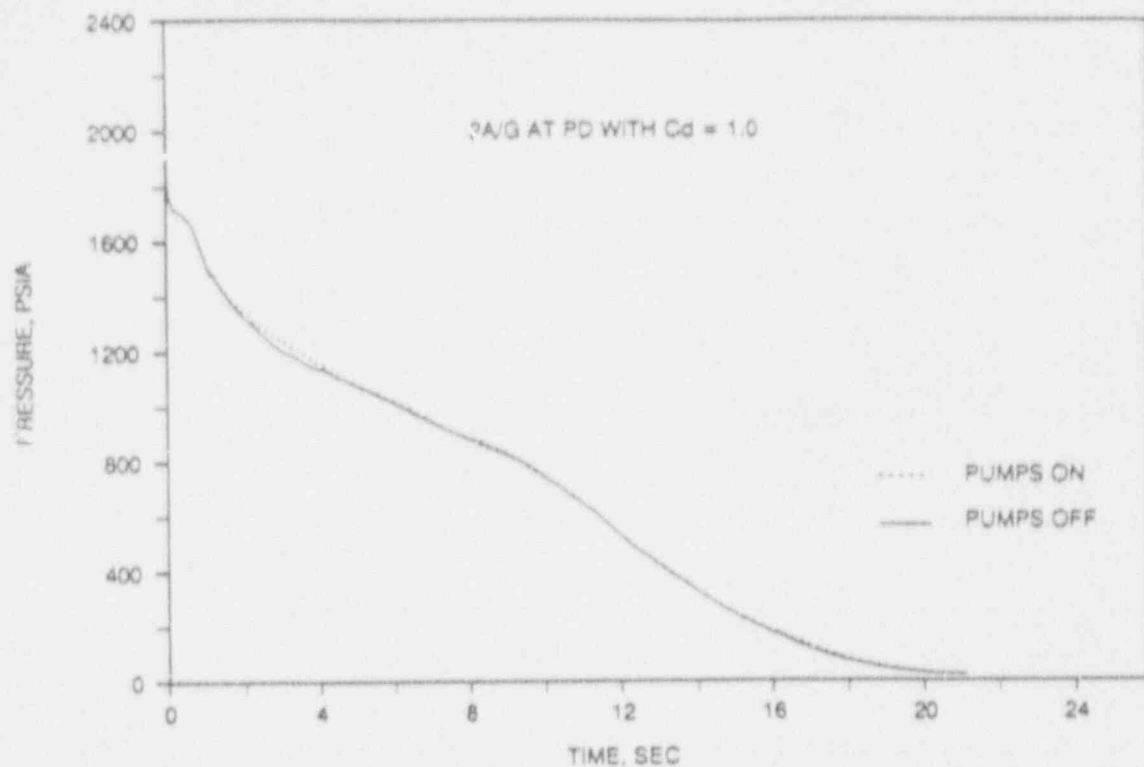


FIGURE 16-2. RELAP5 PUMPS ON/OFF STUDY - LEAK FLOWS AT RV SIDE AND AT PUMP SIDE

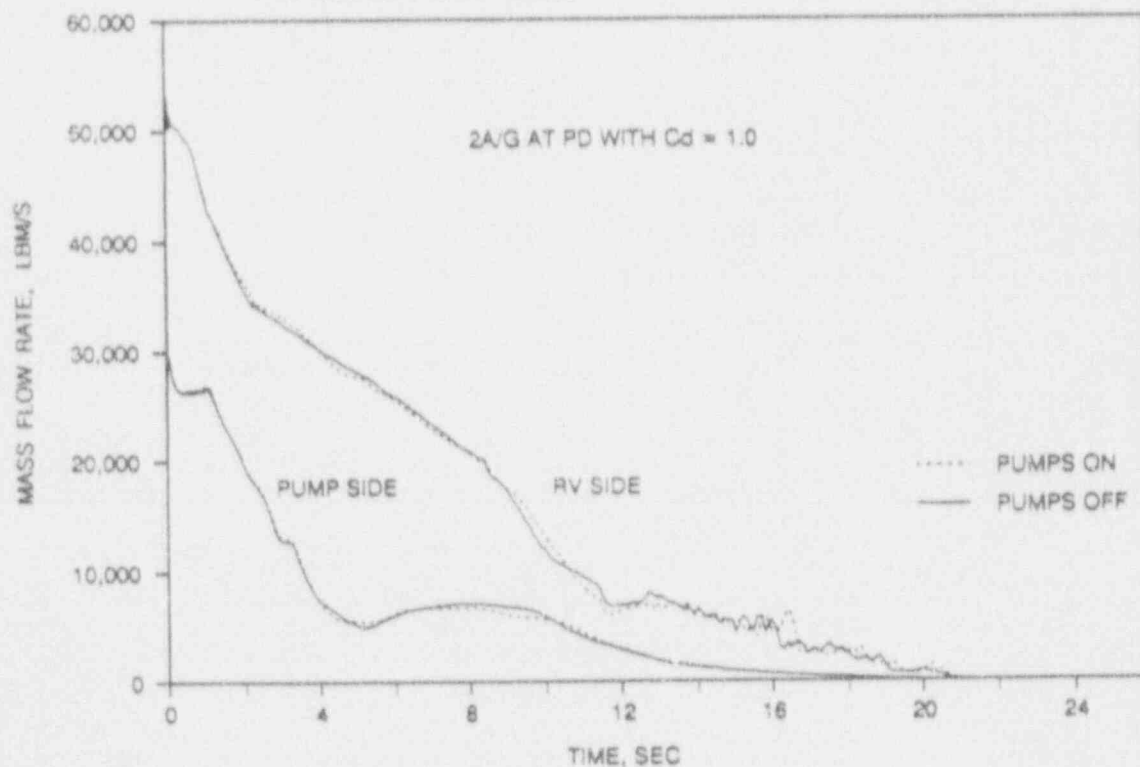


FIGURE 16-3. RELAP5 PUMPS ON/OFF STUDY - INTACT LOOP AND BROKEN LOOP PUMP HEADS

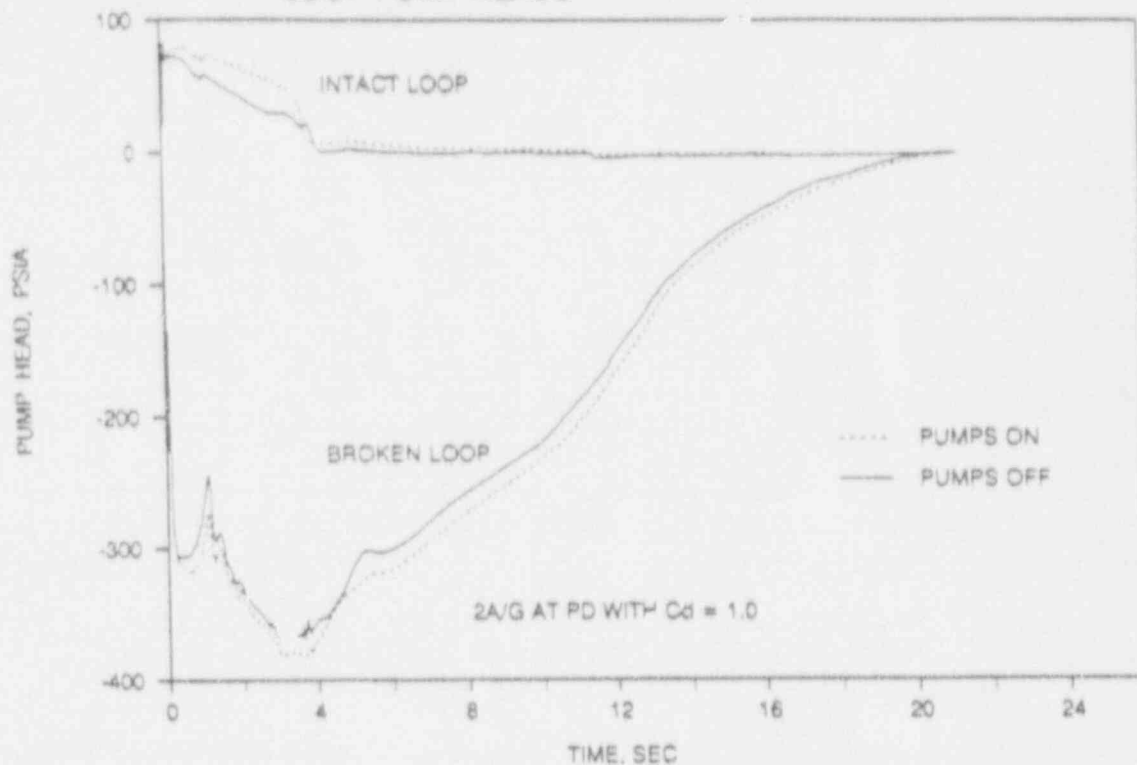


FIGURE 16-4. RELAP5 PUMPS ON/OFF STUDY - FILTERED HOT BUNDLE INLET FLOW

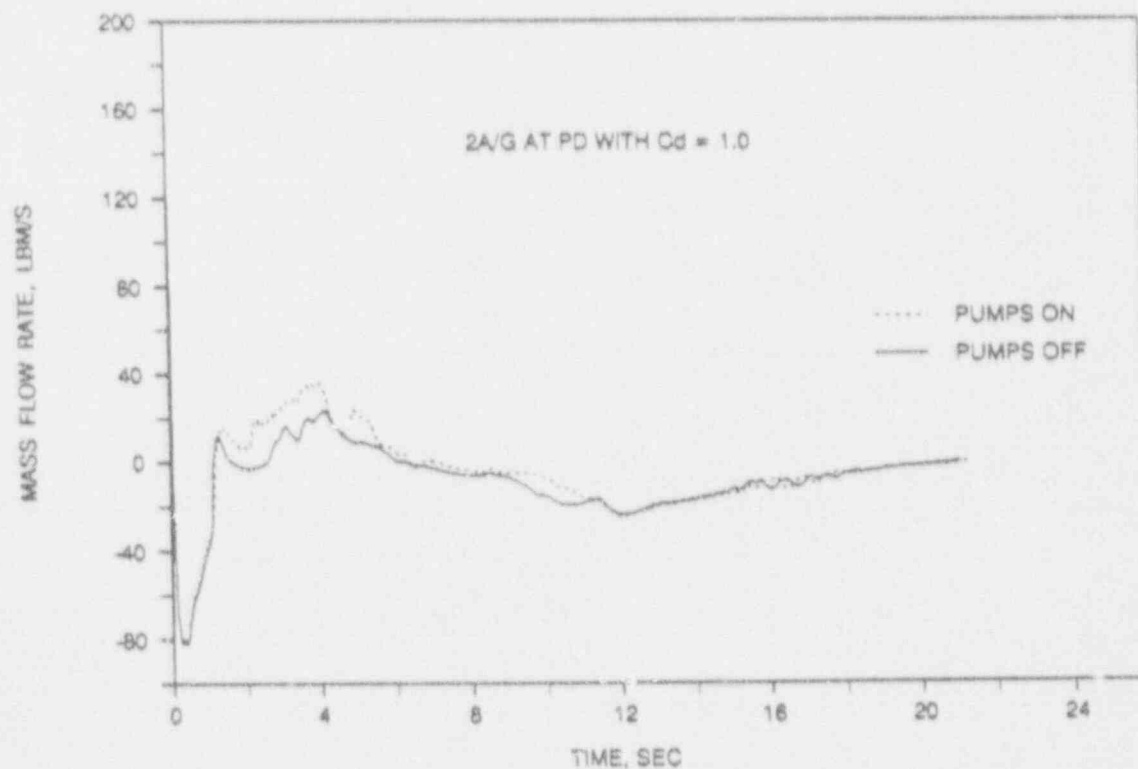


FIGURE 16-5. RELAP5 PUMPS ON/OFF STUDY - HOT CHANNEL CLAD TEMPERATURE AT PEAK POWER ELEVATION.

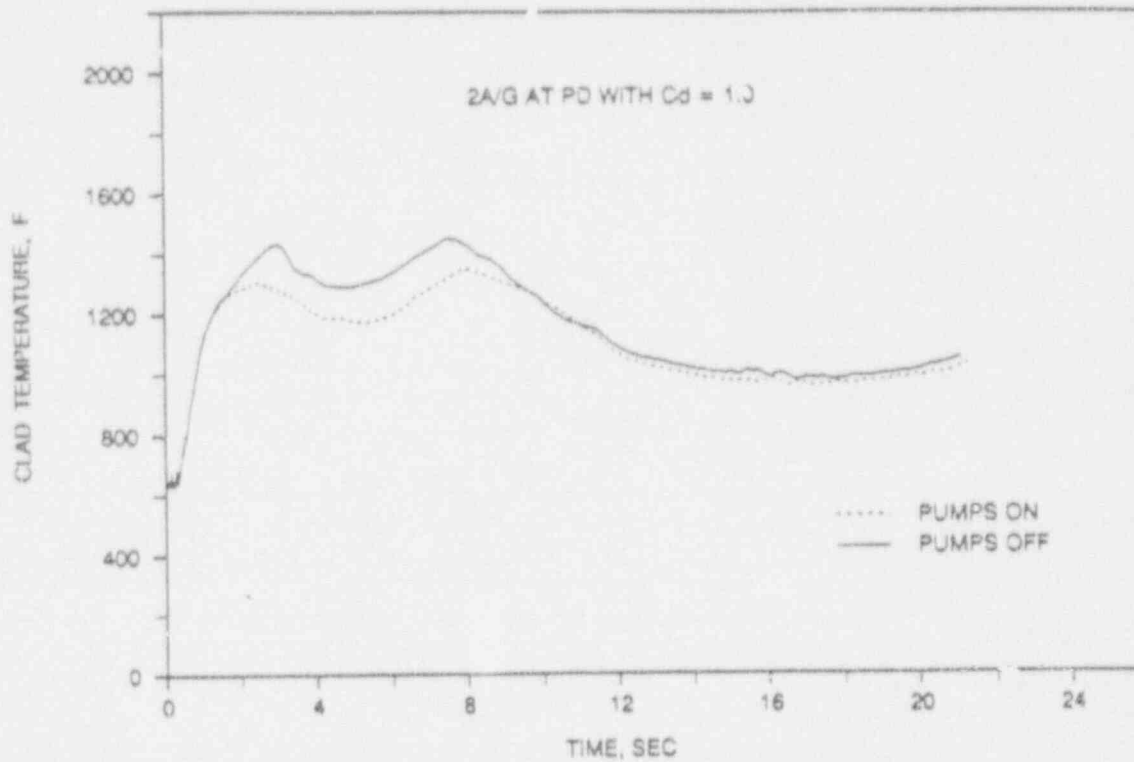
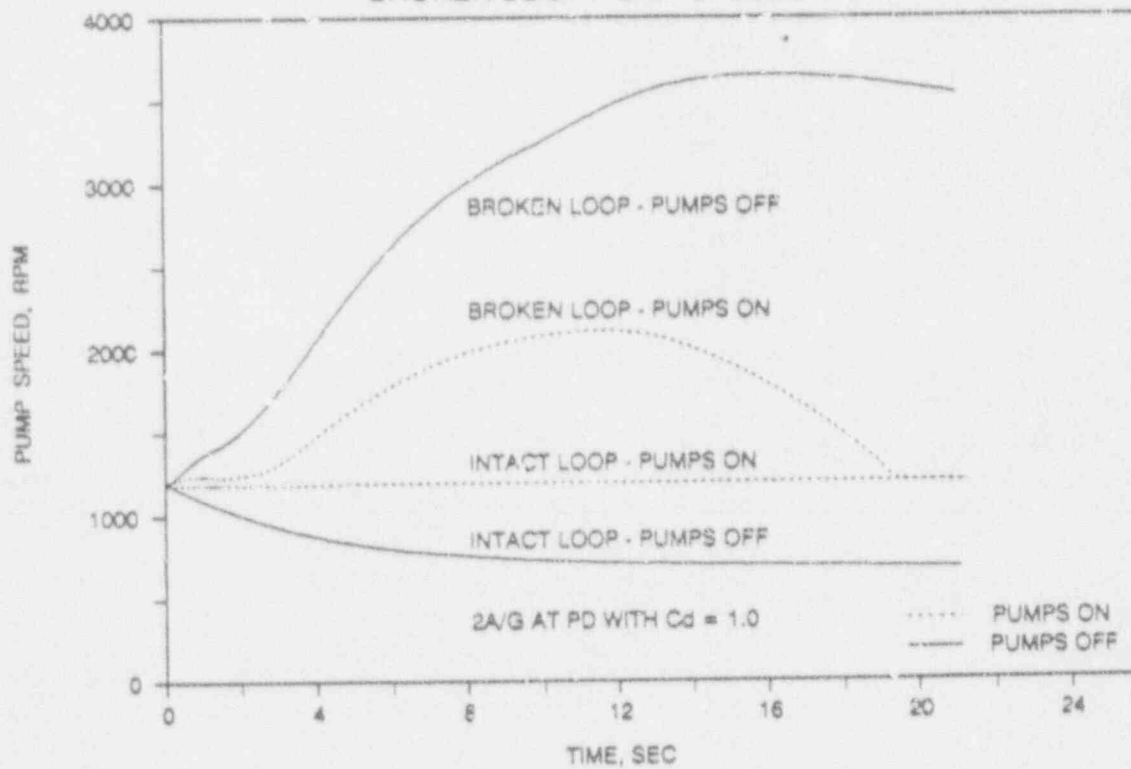


FIGURE 16-6. RELAP5 PUMPS ON/OFF STUDY - INTACT LOOP AND BROKEN LOOP PUMP SPEEDS



17. In Figure 8-22, what caused the oscillations in the heat transfer coefficient at 300 s? Did these oscillations affect the calculation of PCT for the 9.7 ft axial peak case? If so, justify the PCT was not underpredicted for this case.

Response: The cause of the oscillations is described in detail in the response to question 11.d. The PCT reported for the 9.7-ft LOCA limits case is the maximum of a spike resulting from these oscillations. In that sense, the PCT is affected. However, since the oscillations are not physical, they are not important factors in the evaluation of the peak clad temperature. In benchmarks of the BEACH computer code to REBEKA tests, it has been shown that peak clad temperature predictions are conservative by a significant margin. This margin is much larger than the magnitude of the oscillations. Any inaccuracies induced by the oscillations are, therefore, overshadowed by the conservatism built into the model.

18. Although the cladding temperature figures in Chapter 8 show the calculation of the PCT and the turnover of the cladding temperature excursion, the figures do not show a clear cooling trend after the PCT is calculated and they do not show when the rods quench. How do you ensure that reheat will not occur and therefore the peak cladding temperature has been determined to stop further calculation?

Response: Reflood cladding temperatures are calculated by BEACH, which contains suitable conservatism for use in Appendix K calculations up to and after the time of peak cladding temperature. Following the peak, during cladding cooldown and quench, however, BEACH becomes substantially over conservative. Although BEACH would predict a second or third peak in cladding temperature, should cooling conditions degrade sufficiently, it is unable to obtain the heat transfer improvements necessary to predict realistically more rapid cooldown and quench. To assure that the actual peak in cladding temperature has been determined, the BEACH simulation is extended past the time at which the core is quenched as measured by the REFLOD3B code. Once REFLOD3B shows quenching the transient is considered over. A further discussion of this application of REFLOD3B is presented in the response to Question 2 of the second set of questions on the RSG LOCA evaluation model, BAW-10168. Although BEACH under predicts the improvements in cooling following the occurrence of peak cladding temperatures, it does show a continuous increase in cooling when conditions warrant. Therefore, BEACH can be applied to assure that cooling conditions between the time of peak temperature and core quenching continue to improve and no secondary temperature excursion occurs.

Benchmarks of BEACH against experimental data are described in Appendices C, D, and E of BAW-10166P. The benchmarks conclude that, during reflood, BEACH overpredicts post-peak cladding temperatures by a significant margin. Delayed quenching and cladding temperature overprediction, in the post peak period, are prevalent in the FLECHT, FLECHT-SEASET, CCTF, and the



REBEKA-6 benchmarks. In many of the benchmark comparisons, neither quenching nor a substantial cooling trend is evident in the predictions for the duration of the analysis. These benchmarks provide a clear measure of the degree of overconservatism embodied in the present BEACH formulation.

The benchmarks also provide evidence that, provided conditions warrant, clad reheat will not occur after the reflood temperature has reached its peak. While the BEACH results indicate slow post-peak cooling of the clad, experiments show that the clad both cools and is quenched following the peak temperature.

Benchmarks show that BEACH will predict the peak clad temperature and will conservatively overpredict temperatures thereafter. Even though BEACH is unable, for the most part, to predict post peak cooldown and quench, it will predict the possibility of a secondary cladding temperature excursion should cooling conditions degrade sufficiently. Furthermore, experimental observation support the essentially monotonic cooldown and quench of the cladding following the occurrence of peak temperature. Therefore, using BEACH for the determination of peak cladding temperature and to monitor cooling conditions during the post peak period, while employing REFLOD3B to determine the core quench time, provides assurance that the transient has been tracked to completion and the peak in cladding temperature determined conservatively.

19. Chapter 10 discussed the effects of LOCA and seismic loads on the fuel bundle geometry and stated that only mechanical loads were considered because only the outer fuel assemblies are affected and these assemblies do not achieve rupture conditions. To verify this statement, provide a comparison of the average core temperature to the rupture temperature for the LOCA limit cases analyzed in Chapter 8.

Response: The following table displays the requested information.

LOCA limit case	Rupture Temperature (F)	Average Core Temperature (F)
2.9'	1595	1190
4.6'	1609	1228
6.3'	1615	1223
8.0'	1620	1265
9.7'	1602	1231

The rupture temperature represents that local cladding temperature in the hot rod evaluation at which rupture occurs. Average core temperature is indicative of the maximum cladding temperature resulting from the REFLOD3B evaluation and is not, radially, location specific.

20. Chapter 12 discussed the differences between the Mark-BW fuel and the Westinghouse Optimized Fuel Assembly (OFA) and the effects of these differences on small break LOCAs (SBLOCAs). It stated that the difference in initial stored energy between the two assemblies would not affect SBLOCA analyses because the additional energy in the Mark-BW fuel would be removed during the pump coastdown phase of the LOCA and the core energy content during the loop draining and core boiloff phases would be identical to the current licensing base. Provide additional, quantitative data and/or analyses to justify this statement. Compare the initial stored energy of the two fuel types at the start of the SBLOCA and at the start of the core boiloff phase.

Response: The removal of the initial core stored energy occurs during the early phase of a small break LOCA, just following reactor trip in the FSAR calculations for both McGuire and Catawba. Heat transfer during pump coastdown is sufficient to reduce the cladding and fuel temperatures to those required for the removal of decay heat. The cladding is within a few degrees of the system saturation temperature, and pool boiling or natural circulation heat transfer is sufficient to keep them there.

This is evident in both FSARs, as cladding temperatures are not even reported prior to the time of core uncovering, and the temperature excursions initiate from the system saturation temperature at the time the core mixture level falls below the location being reported. As an example, Figure 15.6.4-48 of the McGuire FSAR, the cladding temperature for the worst case SBLOCA, is reproduced here as Figure 20-1 with the saturation temperature at the time of temperature excursion initiation added. Such results are also observed in B&W calculations of SBLOCA.

In both the B&W evaluation model for B&W designed plants and in the BWFC evaluation model for RSG plants, the calculation of cladding temperature is not performed unless there is an indication that the core has uncovered. This follows NRC acceptance of the position that the cladding temperature

closely follows the system saturation temperature prior to core uncover. The same effect is observed experimentally, as described for the Semiscale facility in the following text.

#### S-LH-1 Results for Early SBLOCA Period

The early cooldown of the cladding and fuel temperatures can be seen in the Semiscale S-LH-1 test results and the benchmarks of that test. The cladding temperature response can be seen directly from the data, while the fuel response must be inferred from the benchmark. The benchmark of S-LH-1 is documented in the response to Question 5 of the second round questions on BAW-10164. As with the McGuire and Catawba FSAR analyses, the report on S-LH-1 omits the early cooldown period from the presented data. Figures 20-2 and 20-3 show the cladding temperature at the 8- and 10-ft levels for the first 400 seconds of the test with the data beginning at 100 seconds. The initial energy removal pump coastdown period extends to about 100 seconds. The temperature excursions starting at 150 seconds are related to loop seal clearing and occur following cessation of natural circulation. Figure 20-4 is a reproduction from Reference 20.1 showing a comparison of the results from S-LH-1 and S-LH-2 with data from the initiation of the event. In Figure 20-4, reactor trip is simulated at about 22 seconds. The fuel average temperature profile at the peak power location (6.2 feet above the bottom of the active core) from the S-LH-1 benchmark is shown in Fig. 20-5. In all figures, the fuel and cladding temperatures decrease rapidly following the reactor trip at 22.3 seconds because the cladding surface heat transfer coefficient is more than sufficient to remove decay heat and stored energy in the heater rods. The stored energy is essentially removed within 30 seconds of reactor trip, and thereafter the fuel average temperature remains approximately 50 F above the saturation temperature.

### Fuel Comparison

The contrast between the OFA and the Mark-BW designs can be characterized through examination of geometric parameters. The geometric differences are:

	<u>Mark-BW</u>	<u>OFA</u>
Pin OD (in)	0.374	0.360
Clad Thickness (in)	0.024	0.0225
Pellet OD (in)	0.3195	0.3088
Diametral Gap (in)	0.0065	0.0062

The cladding surface area for the Mark-BW is 4 percent larger than for the OFA, therefore the cladding temperatures at both accident initiation and the time of core uncovering will be slightly lower than those of the OFA. The cladding thickness is 7 percent higher in the Mark-BW design, and the gap is about 5 percent larger. These dimensional increases, combined with the 3.5 percent increase in fuel pellet radius, indicate that the Mark-BW internal temperatures would tend to be somewhat higher than those of the OFA. However, other design considerations, such as gap gas composition and materials properties, lower the difference expected in volume average absolute temperatures to less than 1.0 percent. An assessment of the fuel volumes shows that the fuel pellet volume is 7 percent larger for the Mark-BW design, and the cladding volume is about 11 percent higher. Therefore, it is reasonable to treat the Mark-BW and the OFA designs as having equal internal temperatures and the Mark-BW as having 7 percent higher total stored energy.

### Impact on Transient

Experimental observations and a variety of calculational results (Westinghouse and B&W), some performed on designs



essentially identical to the Mark-BW, show that the cladding and fuel are cooled to near the saturation temperature during pump coastdown. Thus, it is concluded that the differences between the OFA and the Mark-BW designs are insufficient to impede the removal of fuel stored energy during the early phase of the accident. Furthermore, the amount of additional energy associated with the Mark-BW over the OFA amounts to only about 1 percent of the total system energy and its removal will not substantially alter the transient during this period.

During core uncover, the cladding temperature excursion is governed by decay heat levels and core mixture levels. The decay heat levels between the fuels are identical because both will be operated at the same initial power levels. Mixture levels will not vary substantially because of fuel design. Therefore, with both designs initiating the cladding temperature excursion from saturation the Mark-BW will increase and decrease temperature more slowly, higher fuel heat capacity and higher heat transfer during heatup with the heat capacity dominating the heat transfer during cooldown. Thus, there will be little impact on the resultant temperature excursions.

#### CONCLUSION

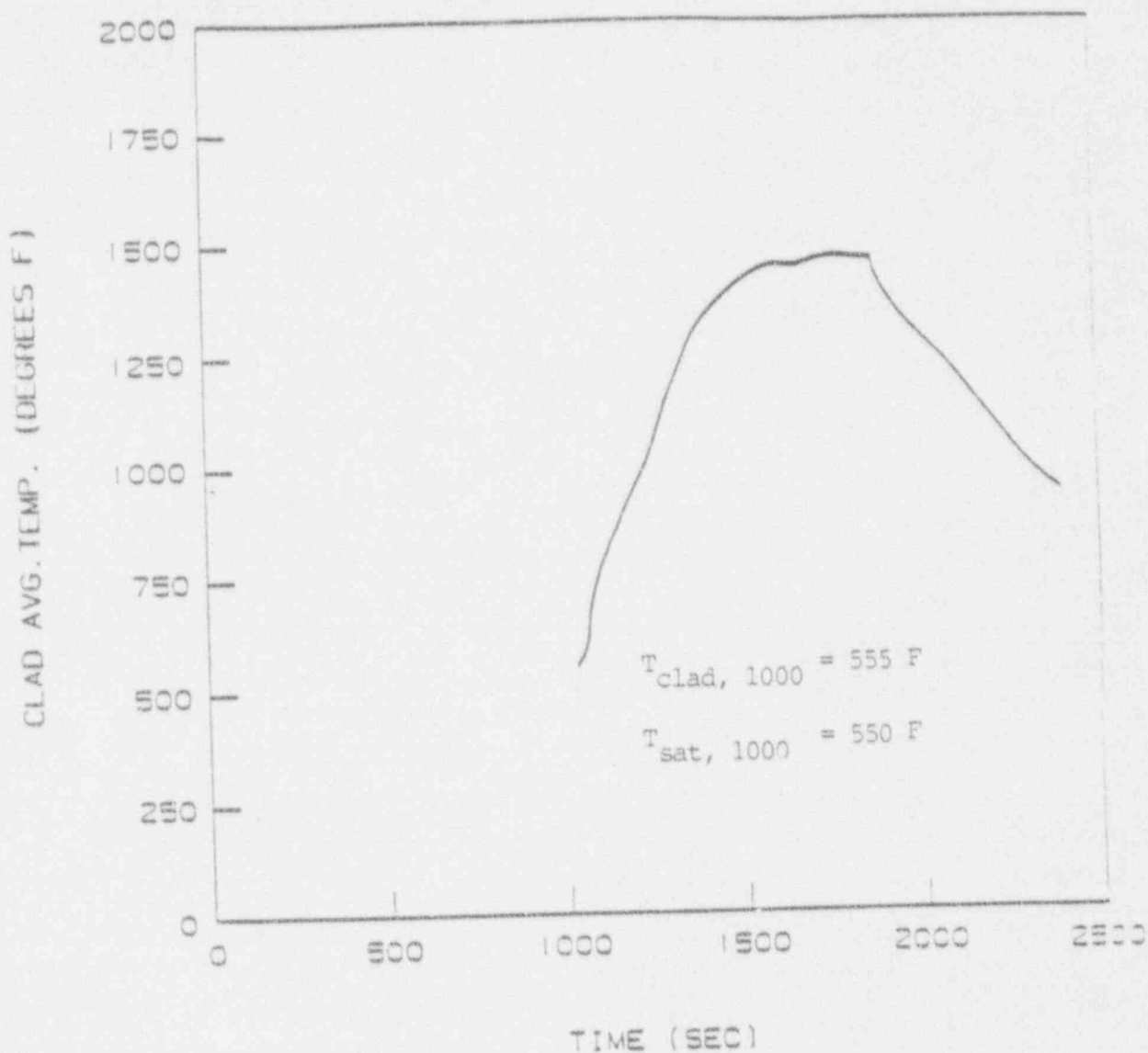
The reduction of fuel rod stored energies during pump coastdown to levels required for the continuous removal of decay heat is well established. The initial temperatures and stored energy of the Mark-BW fuel, although slightly higher than that of the OFA design, will not significantly affect the process. Therefore, under this more detailed examination, the conclusions of Chapter 12 regarding the initial reduction of the fuel cladding and pellet temperatures remain well founded.



References:

- 20.1 G. G. Loomis and J. E. Streit, Results of Semiscale MOD-2C Small-Break (5%) Loss-of-Coolant Accident Experiments S-LH-1 and S-LH-2, NUREG/CR-4438, November 1985.

Figure 20-1 Hot Spot Clad Temperature, 3 Inch Cold Leg Break, McGuire



McGUIRE NUCLEAR STATION  
Figure 15.6.4-48  
1987 Update

FIGURE 20-2. SEMISCALE S-LH-1: 8 FOOT HEATER ROD SURFACE TEMPERATURE

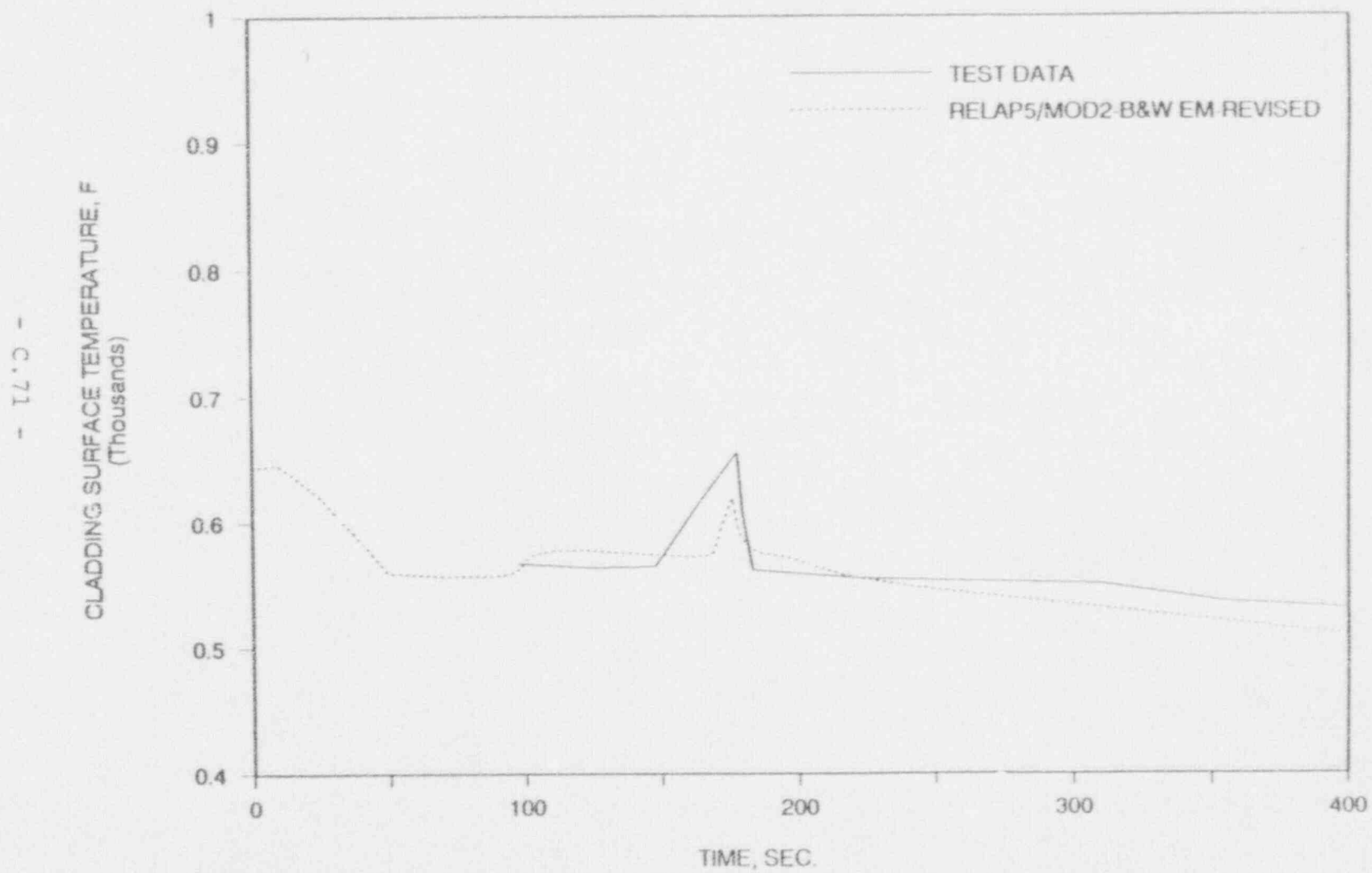


FIGURE 20-3. SEMISCALE S-LH-1: 10 FOOT HEATER ROD SURFACE TEMPERATURE

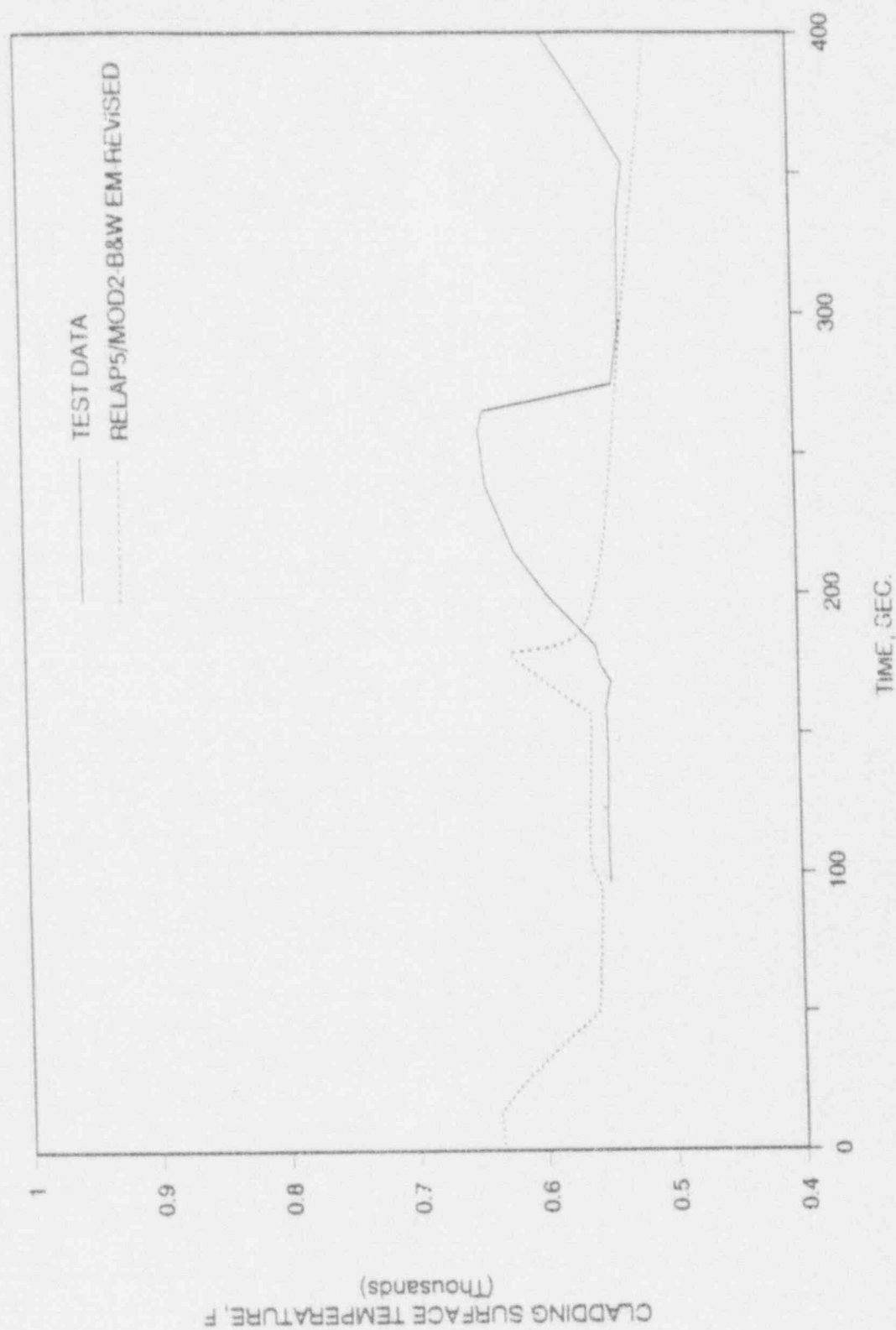
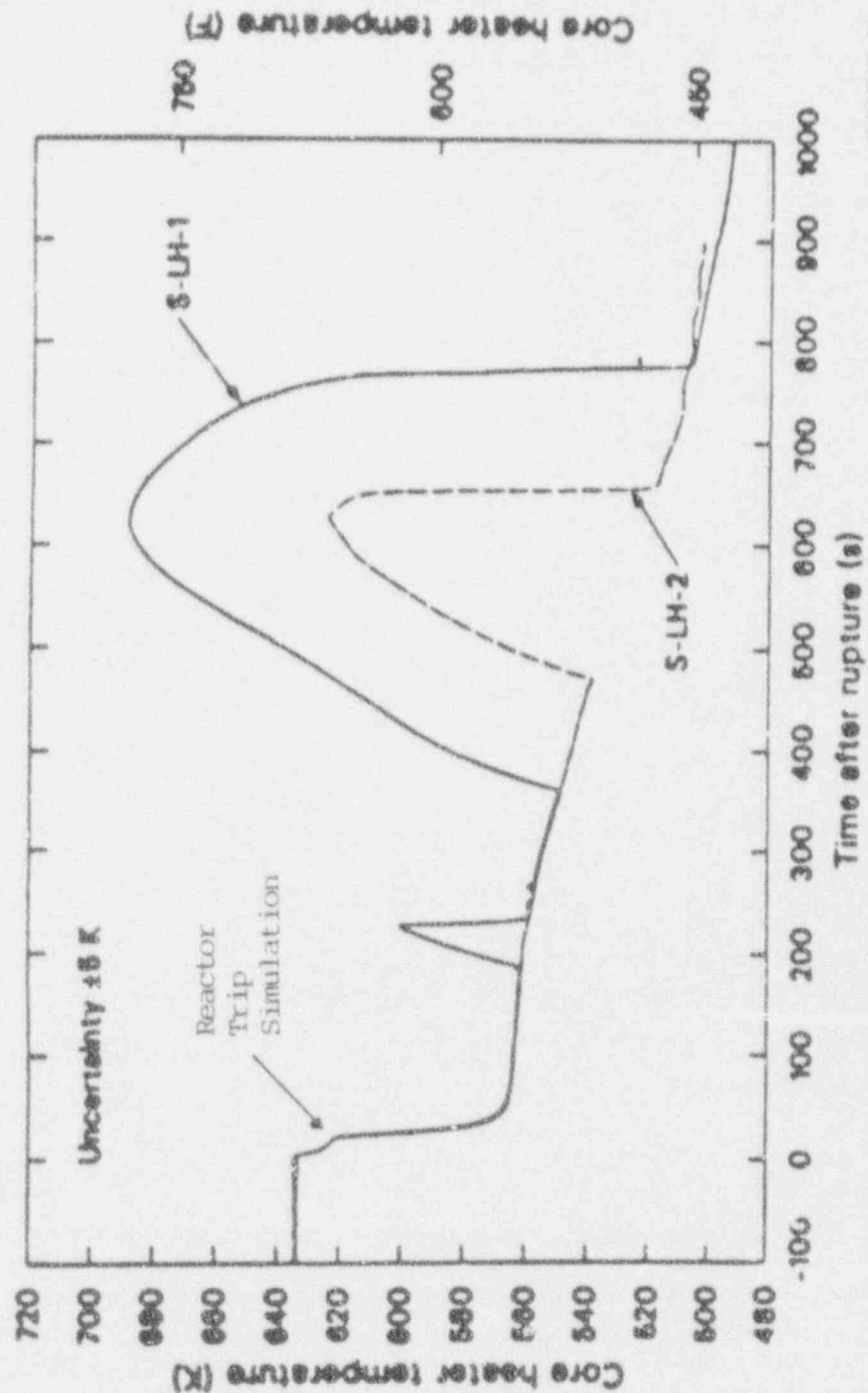
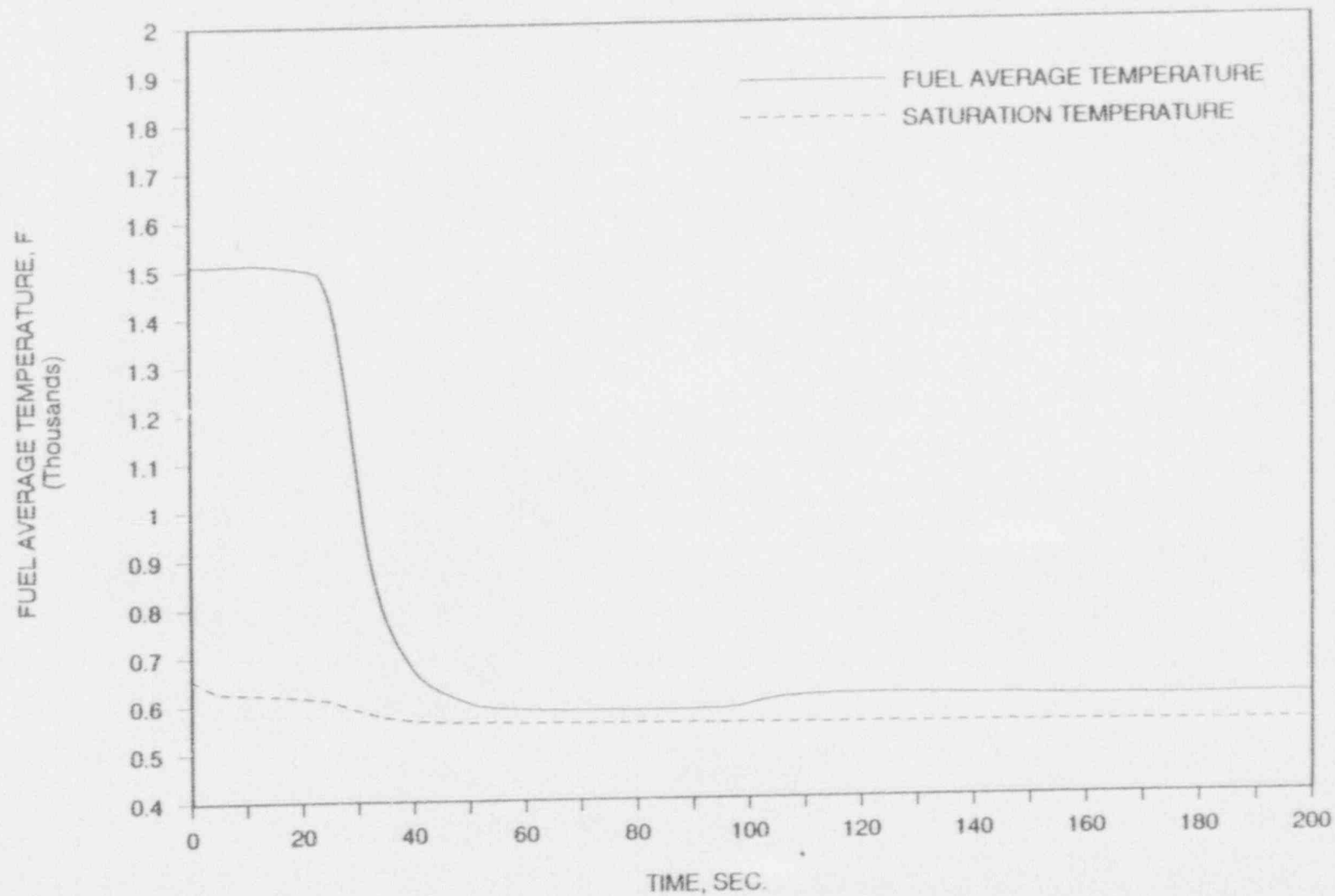


Figure 20-4 Cladding Temperature For S-LH-1 and S-LH-2 Tests  
(Figure 57 of Reference 20.1)



57 Core heater rod temperature during 5% SBL(OCA Experiments S-LH-1 (0.9% bypass flow) and S-LH-2 (3.0% bypass flow)

FIGURE 20-5. SEMISCALE S-LH-1 BENCHMARK: FUEL AVERAGE TEMPERATURE  
AT PEAK POWER LOCATION





21. Pages 12.5 and 12.6 identified the axial power profile as one difference between the OFA and Mark-BW fuel assemblies. Compare the axial profile of the two assemblies. Also, compare the axial profile used for the Westinghouse SBLOCA analyses to the axial profile of the Mark-BW fuel assembly to verify the Westinghouse profile is still bounding.

Response: The identification of the axial power profile was to assure a comprehensive listing of the substantial differences between the OFA and Mark-BW fuel assemblies. The axial power profile at any given time depends more on plant design and operation than on fuel assembly design. Although the two assemblies will differ in power shape for specified plant configurations, the family of possible power shapes is essentially the same for both. Furthermore, the shape at which SBLOCA should be evaluated is selected more to assure a bounding SBLOCA evaluation than in anticipation that any fuel assembly would experience the shape. Therefore, a comparison of the axial power profiles for the two fuel assemblies would be meaningless relative to determining possible differences in response to SBLOCA.

The axial profile under which SBLOCA is evaluated is skewed toward the core outlet to a degree that it would be very unlikely that any real power shape would peak at a higher core elevation. Double peaked shapes are not allowed and the total peak is pushed to the limit of local power at the elevation of the peak. The power shape selected for use in the BWFC small break LOCA evaluation model was presented in the response to question number 46 of the first round of questions on the BWFC LOCA evaluation model topical BAW-10168. This shape is identical to that used in the present McGuire/Catawba SBLOCA evaluations (see Figure 15.6.5-43 of the Catawba FSAR 1987 update). Furthermore, SBLOCA imposed plant operating limits, including maximum allowable total peaking, are not being changed as a result of replacing OFA fuel assemblies with Mark-BW assemblies. Thus, there is no difference in the power shapes used for SBLOCA evaluations of the two assemblies.

22. To justify the use of the FSAR SBLOCA analyses for the Mark-BW fuel reload, clarify whether the ECCS pump flows, ECC water temperatures, boron concentrations, tube plugging, flow resistances and bypass flow, etc, have been reviewed to determine if they are consistent with technical specification limits or requirements, or changes to system geometry since the FSAR analyses were completed.

Response: The evaluations of SBLOCA for operation of the Catawba and McGuire units with Mark-BW fuel were based upon the current reference licensing bases comprising the updated FSARs, technical specifications, and plant-related data supporting the FSAR SBLOCA results. In the strictest sense, the evaluations and the conclusions drawn in Chapter 12 must be considered as anticipatory. That is, they consider conditions that are expected to pertain and presume that any changes to these conditions at the time of the reload--modifications to plant geometry, ECC flows and performance, flow resistance, and like parameters--are such that the evaluations and conclusions remain valid. Each cycle-specific safety review examines the plant and core parameters to reconfirm that the FSAR and such supplementary analyses and evaluations as those in BAW-10174 are applicable to that particular cycle.

The ECCS pump flows, ECC water temperatures, and boron concentrations used in the existing FSAR small break LOCA analyses have been reviewed. Any technical specification changes necessary to be consistent with these analysis assumptions will be proposed for the first McGuire or Catawba reload with Mark-BW fuel. The current McGuire FSAR SBLOCA analysis has recently been reevaluated for 10 percent steam generator tube plugging, and the McGuire FSAR will be updated accordingly. The current Catawba FSAR SBLOCA analysis already assumes 10 percent tube plugging.

It is anticipated that, coincident with the loading of Mark-BW fuel at McGuire Unit 1, the core bypass will be changed from

a downflow configuration to an upflow configuration. This alteration has been reviewed in terms of potential impact upon SBLOCA calculated results and concluded not to affect the peak cladding temperatures or other applicable ECCS criteria.

The cladding temperatures during small break LOCA calculations are governed by hydrostatic considerations in the reactor coolant system during the core boil-off period. During the boil-off period, system flow is sufficiently low that even large loop resistance variations are inconsequential. The loop resistance used in the current FSAR SBLOCA evaluation has been reviewed and approved, and there have been no changes in system flow or total loop differential pressure since that analysis. The loop resistances will not be altered by the change in fuel type. Therefore, steam venting during boil-off will not be affected. Perhaps because of the reduced core inlet resistance for the Mark-BW, the head required may actually fall (the difference is most likely too small to notice).

To reiterate, all plant and technical specification modifications are reviewed for impact and consistency with bases and licensing documentation. Most of these have no impact on SBLOCA results. Those that could potentially alter the SBLOCA calculations have been evaluated for consequence and will either be reported in the FSAR or have been found to not impact the calculated results of the reference FSARs or the conclusions drawn in Chapter 12 of the topical report.

23. The size of the fuel rod in the Mark-BW and OFA assemblies are different with the Mark-BW fuel rod being slightly larger. During the core boiloff phase of a SBLOCA, the effect of the larger fuel rods would be to cause the core to uncover earlier and more quickly because there is less water in the core to boiloff. Given this consideration, justify the FSAR SBLOCA analyses for Catawba and McGuire are still limiting with respect to use of Mark-BW fuel.

Response: Table A-1 of BAW-10174 provides basic geometric information for the OFA and the Mark-BW designs. The most significant difference between the two designs is the outside diameter of the fuel pin cladding. For the OFA this dimension is 0.36 inches and for the Mark-BW it is 0.374 inches. That change reduces the inventory of liquid containable in the reactor vessel core but does not lead to accelerated core boiloff as indicated in the question. Dimensional changes in the thimble and instrument tubes are not as large as those for the fuel pin allowing those effects to be considered as a perturbation of the fuel pin diameter change. For this answer the entire core region will be considered as reduced in liquid volume by the percentage change in the unit cell flow path area from the OFA to the Mark-BW assembly.

The effects of a decrease in core flow area caused by the increase in cladding outside diameter on a small break LOCA transient are limited to: (1) a change in minimum core mixture level, (2) a more rapid dropping of the core mixture height while the core is uncovered, (3) increased refill rates while the core is uncovered, (4) altered steam flow in the upper region of the core associated with the change in core mixture level, (5) altered vapor superheating at the location of peak cladding temperature associated with the change in core mixture level and steam flow, (6) altered heat transfer in the steam cooled region associated with the change in steam mass flow rate and vapor superheat content, (7) increased heat transfer in the steam cooled region associated

with the increase in cladding surface area and the increased vapor velocities caused by the reduced subchannel area.

As detailed in the following paragraphs, the effect of a core flow area decrease on the McGuire and Catawba SBLOCA calculations will be a decrease in vessel liquid inventory at the initiation of the vessel boildown phase of SBLOCA but an increase in the amount of boiling required to achieve the predicted minimum core mixture level. This results in a delay in the timing of the start of core uncover, a delay in the occurrence of minimum core mixture level, and an increase in the value of the minimum level. Thus, contrary to the hypothesis in the question the effect of the reduced core flow area of the Mark-BW fuel will be to shorten the period of core uncover and slightly reduce the peak cladding temperature.

The differing cladding outside diameters cause a reduction in unit cell flow area:

$$a_{\text{unit cell}} = \frac{\text{Flow Area Reduction Factor}}{\text{Flow Area Reduction Factor}} = \frac{(0.374^2 - 0.36^2)\pi/4}{0.496^2 - 0.36^2\pi/4} = 0.056$$

Applying this reduction ratio to the entire core accounts for the less substantial changes in the thimble and guide tube dimensions. The reduction in core free volume is about 38 ft<sup>3</sup>. For an average core void fraction of 30 percent (a reasonable value at the time of the start of vessel boildown) the change in core liquid content is about 20 ft<sup>3</sup>. However, an examination of the vessel liquid distribution at the start of vessel boildown shows that the smaller core flow area causes liquid inventories in the upper plenum to increase by more than the 20 cubic foot reduction in the core region. Therefore, because the upper plenum drains into the core during vessel boildown, the initial uncovering of the top of



the core will take longer for the Mark-BW than for the OFA, and the peak cladding temperature may be reduced.

For the McGuire/Catawba plant design, the inventory at the start of vessel boildown is determined by the hydrostatics necessary to clear a reactor coolant system loop seal and establish direct steam venting from the vessel outlet to the break. To precipitate loop clearing, an elevation head difference is established between the downcomer and the core that is sufficient to cause a similar imbalance in the pump suction. This allows steam to flow into the riser section of the pump suction. Once a loop seal clears (the BWFC SBLOCA model only allows one loop to clear), liquid from the clearing loop pump suction riser section and all four cold leg horizontal sections flows to the core, and the vessel boildown phase of the accident is initiated. At loop seal clearing, the required liquid distribution fills the downcomer and the cold leg piping while the core elevation head is suppressed to about 7 ft (core mixture level will be around 9.5 ft). The effect, at this point, of a change in core area is a small reduction in the core and vessel liquid inventories (about 20 ft<sup>3</sup>).

Following loop seal clearing, water from the cold legs and the downcomer flows into the core and upper plenum to form a new hydrostatic balance, with the elevation head in the downcomer essentially equal to that in the core and the upper plenum. With the smaller core flow area, it takes less liquid to fill the core and the core elevation head is reduced due to a slight increase in the core void fraction (steam production and flow requires that the core void fraction - core flow area product be conserved). Contrasting the OFA and the Mark-BW, the Mark-BW requires about 50 ft<sup>3</sup> less liquid to fill the vessel such that the core is just covered (this includes the effects of core boiling and vessel elevation heads). Because 50 ft<sup>3</sup> less liquid is required to fill the vessel to the point



at which the core is covered, the final liquid distribution will have 30 ft<sup>3</sup> more liquid above the core even with a lower total vessel liquid volume of 20 ft<sup>3</sup>.

The difference in liquid above the core causes a delay in the time at which the vessel mixture level reaches the top of the core. Once the mixture level falls into the core, however, the rate of mixture height decrease will be faster for the Mark-BW. Total depletion of core inventory will occur somewhat sooner in the Mark-BW (total vessel inventory for the Mark-BW is 20 ft<sup>3</sup> less than the OFA and inventories below the core are unaffected by the change in core design). Equality between the two designs occurs when the core mixture height reaches about 5 ft from the bottom of the core. The McGuire and Catawba FSAR analyses show minimum core mixture heights of about 8 ft. Therefore, the minimum SBLOCA core mixture height when the plants are operated with the Mark-BW assembly will be slightly higher and occur slightly later than in the reference FSAR analyses.

The vapor flow rate and superheat considerations correspond directly to the mixture height effects. If the mixture height has been increased, vapor flow will increase, and vapor superheat will decrease. The heat transfer coefficient at the hot spot is vapor velocity-dependent and will tend to increase because of the increased steam mass flow but decrease because of the reduced vapor superheat. The reduced core flow area will act to increase vapor velocities, increasing the heat transfer. The increase in core surface area will also increase heat transfer.

Evaluations of the magnitudes of all of these effects shows that for locations near the core exit (the location of PCT for SBLOCA) the vapor superheat content dominates the determination of cladding temperature, with the other effects being relatively smaller. Additionally, for the change from

OFA to Mark-BW, studies show that the potential impact, although positive, is very limited. The expected improvement in peak cladding temperature is no more than 10 F or 20 F and may not even be observable at the calculational level of present evaluation models. The limited nature of the impact of the switch from the OFA to the Mark-BW is further supported by alternate approaches that ignore the positive effect of the smaller core flow area by assuming that the top of core is uncovered at the same time for both designs. Under this assumption, the peak cladding temperature for McGuire would increase by only 15 F. Thus, even though the impact of the core change will be positive on the peak cladding temperature (decrease cladding temperature), the effect is so limited that it is inconsequential.

Chapter 12 of BAW-10174 assesses various potential impacts on small break LOCA calculations of the change from the Westinghouse supplied OFA fuel assembly to the BWFC supplied Mark-BW assembly. None of the effects assessed were considered to represent substantial impacts to the present FSAR SBLOCA calculations, and those calculations were established as applicable to and bounding for the Mark-BW fuel. This obviates the need for performing Mark-BW specific SBLOCA calculations. The effect of the cladding outside diameter increase on the FSAR calculations was not considered in the Chapter 12 evaluation and probably should have been. The assessment of the core flow area change provided here shows that the cladding temperature will be decreased by the change from OFA to Mark-BW. The magnitude of the change, produced by a more favorable distribution of liquid within the reactor vessel at the initiation of vessel boildown, will be small and inconsequential. Including the effect of the increase in cladding diameter does not alter the conclusion of Chapter 12 that the FSAR analyses for SBLOCA are applicable and bounding for the Mark-BW fuel assembly. Therefore, as stated in Chapter 12 of BAW-10174, the FSAR calculational

results for SBLOCA can be used to license the plant operations with the Mark-BW fuel.

24. Appendix A of BAW-10174 only discussed the effects of mixed core operation on a LBLOCA. Provide similar information to justify operation of Catawba and McGuire with mixed cores as it relates to a SBLOCA.

Response: Chapter 12 of BAW-10174 addresses the differences between the Mark-BW and the OFA fuel assemblies in regards to small break LOCA calculations. Question 23, of this set, considers a design difference between the fuel assemblies that was not included in Chapter 12. Both discussions conclude that the change from the OFA assembly to the Mark-BW will not alter the SBLOCA calculational results and that the OFA based FSAR analyses may be applied for licensing of the Mark-BW fuel. As will be presented here, that conclusion also applies to the mixed core conditions during the transition from the OFA assembly to Mark-BW. No interaction between the OFA and the Mark-BW designs has been identified that will alter or compromise the SBLOCA calculational results.

Chapter 12 identified the following differences between the OFA and the Mark-BW fuel assembly designs as possibly affecting SBLOCA calculations: unrecoverable pressure drops across the assemblies, initial fuel temperatures, initial pin internal gas pressure, and the axial power profile. Appendix A of BAW-10174 listed geometrical differences between the fuel assembly designs along with the unrecoverable pressure drops. Table 24-1 has been copied from Appendix A to show the differences. Each of the differences between the OFA and the Mark-BW designs has been examined for possible adverse effects during mixed core operation, and none have been identified.

#### Unrecoverable Pressure Drop

As discussed in the response to Question 22 of this set, cladding temperature excursions during small break LOCA calculations occur during the core boiloff period and are governed by hydrostatic considerations. The only resistance-

related pressure drops of significance occur within the steam regions of the RCS to vent steam from the top of the core to the break. With these, system flow is sufficiently low that even large loop resistance variations are inconsequential. The pressure drop difference between the OFA and the Mark-BW occurs at the fuel assembly inlet with operational flows. During the core boiloff period this location will be under water with flow rates so small that flow related pressure changes are inconsequential. The response to Question 22 allowed that some benefit " ... most likely too small to notice ... " might occur because of the decreased resistance of the Mark-BW. However, because of the hydrostatic balancing across the core, the benefit would apply to all fuel assemblies, not just to the Mark-BW. Therefore, the decreased inlet pressure drop of the Mark-BW fuel assembly does not interact with the OFA assembly to create any adverse consequence for SBLOCA during mixed core operation.

#### Initial Fuel Temperatures

The issue of initial fuel stored energy was addressed in Chapter 12 of BAW-10174 and has been further discussed in the response to Question 20 of this set. The Mark-BW fuel pellet can be expected to contain approximately 7 percent more energy at operation than the OFA. This extra energy is easily removed during pump coast down and removed via the steam generators (see Question 20 response) and will not affect the response of either fuel assembly during that period. During the vessel boiloff phase the Mark-BW assembly will heat up slightly slower than will the OFA and cool down slightly slower. The effect would be that of a buffer or a filter on the transient occurrences and, although most likely unnoticeable in a calculation, would tend to reduce the amount of core uncovering. Thus, if a coupling can be considered to exist between the OFA and the Mark-BW because of this, the effect would be to reduce the computed cladding temperature



for the OFA assembly. Since the OFA temperature is the temperature of record for the Mark-BW, there is no adverse consequence on either assembly for mixed core operation.

#### Initial Pin Internal Gas Pressure

As discussed in Chapter 12 of BAW-10174, the fuel pin internal gas fill pressures are similar between the two fuel designs. The gas pressure differences that do exist could affect cladding rupture time slightly, but the impact of a rupture or rupture timing difference would be negligible. The occurrence or lack of rupture does not affect vessel inventories and cannot cause any interaction between the two fuel designs. Therefore, although small differences could exist between the OFA and the Mark-BW, these differences will not produce an adverse SBLOCA result for mixed core operation.

#### Axial Power Profile

As discussed in the response to Question 21 of this set, there is no difference between the power shape used for evaluation of the SBLOCA between the OFA and the Mark-BW. Therefore, the potential difference in assembly power shapes for operation not approaching the allowable limits will not affect the results of SBLOCA calculations nor cause a coupling between the two fuel designs which would adversely affect mixed core operation.

#### Geometrical Differences

The geometrical differences between the fuel assemblies and their impact on SBLOCA calculations are assessed in the response to Question 23 of this set. The conclusion is that these differences will cause a negligible though positive impact by slightly increasing the minimum core mixture height. The trend in operation with both fuel designs will be



improvement of results in direct proportion to the amount of Mark-BW fuel in the mixed core. Therefore, although there would be some definite interaction between the designs, the effect will be relatively minor and somewhat beneficial to the OFA results. Since the OFA calculations are the calculations of record for the Mark-BW, there is no adverse consequence on either assembly for mixed core operation.

#### Conclusion

The differences between the OFA and the Mark-BW assemblies are such that small break LOCA cladding temperatures for mixed core operation will not vary substantially from those calculated for OFA core operation. The quantifiable deviations would indicate a slight lowering of the peak temperatures as the core transitions to full Mark-BW. The calculations show considerable margin to 10 CFR 50.46 criteria. Operational limits or technical specifications required by the OFA-based FSAR analyses will not be altered for application to the Mark-BW assembly. Therefore, the FSAR evaluations can be applied to the licensing of both the full Mark-BW core and the mixed or transition core.

Table 24-1 OFA/Mark-BW Design Differences

	MK-BW	OFA
Guide Thimbles:		
Upper Section OD/r (in)	0.482/0.016	0.474/0.016
Lower Section OD/r (in)	0.429/0.016	0.429/0.016
Instrument Tube:		
OD/r (in)	0.482/0.016	0.474/0.016
Fuel Pin:		
Pin OD (in)	0.374	0.360
Clad Thickness (in)	0.024	0.0225
Pellet OD (in)	0.3195	0.3088
Pellet Length (in)	0.400	0.507
Diametral Gap (in)	0.0065	0.0062
Pressure Drop Across Core (psi) (at full flow)	22.7	23.7

25. Section A.2 contained an estimate of the difference in PCT expected in a LBLOCA for the blowdown and reflood phases due to mixed core operation on both the Mark-BW and OFA evaluations. Provide a quantitative justification for the differences in PCT discussed in that section.

Response: The temperature effects described in Section A.2 of Appendix A of BAW-10174 are based on quantitative evaluations. The 30 F to 50 F impact of mixed core operation during blowdown was obtained from an early RELAP5 evaluation of a core configuration consisting of one Mark-BW and 192 OFA fuel assemblies. Although the RELAP5 version, model, and input were not current, their content was sufficient to extract the sensitivity attributed to the mixed core configuration in Appendix A. That case showed that little difference exists between an all Mark-BW core and a core with one Mark-BW and 192 OFA assemblies during the positive core flow period at the beginning of the transient. Once the RC pumps cavitate, however, the core flow becomes negative and of much higher quality. During this period, the higher pressure drop of the OFA diverts a small amount of flow to the Mark-BW, somewhat reducing the cladding temperature for the Mark-BW.

As with blowdown, assessments of the flooding rate results were based on actual REFLOD3B calculations. Because REFLOD3B is a single channel code, the evaluation compared a full OFA core to a full Mark-BW core and then applied the flooding rate deviation to the cooling of the other assembly. The full OFA core was shown to experience a slower, by less than 2 percent, reflooding. In order to assess the single Mark-BW - 192 OFA assembly core, the effect of this lower OFA flooding rate on the reflood cooling of the Mark-BW assembly was determined. Because flooding rate changes of this order of magnitude had been observed in sensitivity studies, no new cladding temperature calculation was necessary. The temperature differences reported are those typical of 2 percent flooding

rate changes in the evaluation model and the McGuire/Catawba sensitivity studies.

Appendix A does not portend that the temperatures provided are exact measures of the effects of interactions between the OFA and Mark-BW assemblies during the transition cores. Rather Appendix A intends to describe the trend which would be observed in a fully implemented calculation, explain why this trend occurs, and provide a conservative estimate of any potential change in peak cladding temperature. The position taken in Appendix A is: (1) Some degree of interaction can occur between the OFA and the Mark-BW fuel assemblies for the transition cores. (2) The variation of cladding temperature caused by the interaction will trend in opposite and offsetting directions between the blowdown and the reflood periods. (3) The variations will be limited to less than 50 F during either the blowdown or reflood periods with the net difference at peak temperature being near zero. (4) Because of the limited potential interactions and the margins to limiting criteria available within the whole core calculations, no specific evaluation model computation of the mixed core configuration is warranted.

26. Because the hot rod analysis using the BEACH code during reflood includes rod-to-rod radiation heat transfer, provide the following information:

- a. Clarify if the hot rod in the hot assembly is a center rod or a rod on the outside of the bundle.
- b. If the hot rod is on the outside of the bundle, clarify how B&W determines the limiting radiation enclosure; how B&W handles radiation heat transfer to bundles of different burnup; and radiation heat transfer to different bundle types (i.e., Mark-BW or OFA).

Response: The heat transfer package used by BEACH does not include rod-to-rod radiation. Radiation heat transfer is modelled between the fuel pellet and the cladding of a fuel pin as part of the gap conductivity calculation. Radiation is also modelled from the cladding surface to the coolant under selected conditions as explained in Section 2.2.2 (specifically the introduction to Section 2.2.2 and Section 2.2.2.11) of the BEACH topical report, BAW-10168. Because no rod-to-rod radiation or other pin-position-sensitive processes are given credit in the BEACH calculations, the calculations are considered independent of pin position and described as appropriate for the average pin in the hottest fuel assembly in the core.

27. Page A.4 stated the core inlet flow and core inventory increased by 2 to 5% in the case where the core was assumed to include only OFAs. Clarify why these two parameters increased when the core flooding rate decreased in this case.

Response: The important parameters in determining the increase in the core inlet flow and inventory and the decrease in core flooding rate (inches/sec.) for the OFA fuel assembly are the decrease in pin outside diameter and the increase in inlet flow resistance for that design. The decrease in fuel pin diameter causes the flow area for the OFA core to be about 7 percent larger than for the Mark-BW core. Therefore, if the flooding rates for the two cores were identical, the core inlet mass flow, would be 7 percent higher for the OFA core. Similarly, for the same total carryout and flooding rate, the core inventory would also be 7 percent higher at any given time.

A simplified relationship between the core inlet mass flux,  $G$ , and the downcomer elevation head,  $\Delta P$  is

$$G^2 \propto \Delta P - \text{FRICTION LOSS}$$

Thus, for two cases with the same elevation head and essentially the same friction losses, the mass fluxes at the core inlet are the same. The elevation head is created by the difference between the core level and the downcomer level. For most of the reflood transient, the downcomer is full and the level used in the calculation is constant. Because the core level is determined by the inlet volume flow less the carryout, it will be preserved for a core flow area change so long as the carryout is appropriately correlated, i.e. decreases appropriately with increasing hydraulic diameter. When most of the flow resistance is downstream of the core inlet losses, in the steam binding, none of the terms in the mass flux equation change substantially.



For the change from a Mark-BW to an OFA core using the BWFC REFLOD3B code, the above conditions pertain. The higher fuel assembly inlet resistance causes a slight degradation, about 2 percent, of the mass flux that shows up as a slightly reduced flooding rate. The degree of mass flux change, however, does not offset the increase in core area, and the core mass flow and mass inventory are higher for the OFA than they are for the Mark-BW.

The foregoing applies provided that the ECCS capacity is sufficient to keep the downcomer filled for either core. For McGuire and Catawba, the ECCS rates are more than sufficient to maintain the downcomer full with either core design since between 15 and 25 percent of the low pressure injection is spilled out of the downcomer to the containment even under minimum ECCS assumptions.

Thus the conditions observed at the bottom of page A.4 are those that are reasonable and expected for the type of core change occurring between the OFA design and the Mark-BW. The use of the term "mass" in the core inlet flow and core inventory would have made the meaning clearer in the original text.

28. On page A.3 the statement is made that the only effect of the mixed core that needed to be considered for the reflood phase of the LBLOCA is the whole core pressure drop. The following questions are related to this statement.

- a. Justify why the pressure drop difference for the two fuel assemblies does not cause a flow diversion effect during the reflood phase similar to the blowdown phase. Provide appropriate data or analyses to support your conclusions.

Response: The large break LOCA reflood phase is modelled with a one-dimensional simulation that conservatively ignores radial effects within the core. Some flow diversion toward the Mark-BW is likely and could be represented in more detailed modelling in a larger calculation. Those techniques would also substantially reduce reflood cladding temperatures by incorporating two- or three- dimensional effects that cause preferential flow to the hot assemblies. Because it would be inconsistent to evaluate a negative multi-dimensional effect without consideration of the positive effects, only the overall resistance impact of the OFA versus Mark-BW assemblies was included in the reflooding comparison.

The combined effects of added assembly resistance and two- or three- dimensional reflooding can be discussed considering two possible core arrangements: (1) a Mark-BW assembly as the hot assembly surrounded by OFA assemblies or (2) an OFA assembly as a hot assembly surrounded by Mark-BW assemblies. In either situation the flow diversion, if any, will be toward the Mark-BW. Therefore, with diversion allowed, the cooling of the Mark-BW assembly under the first arrangement would be enhanced over present evaluations and that condition need not be considered further.

For the second core arrangement, with an OFA assembly as a hot assembly, any flow diversion toward the Mark-BW

would tend to reduce the flow of water entering the bottom of the OFA assembly. The pressure drop difference between the OFA and the Mark-BW is small (less than 5 percent), and any flow diversion toward the Mark-BW would be limited to the square root of that difference (about 2 percent). Such a diversion is not large and would be compensated for by a buildup of elevation head in the Mark-BW assemblies. The pressure drop across the nominal core during reflooding is less than one psi. Converting 5 percent of the core pressure drop into elevation head shows that the water level in the Mark-BW need be only 0.12 feet higher than that in the OFA to eliminate the diversion of flow. At 2 percent flow diversion that difference in elevation head will be established within the first 30 to 40 seconds of the reflooding transient. Thus, any net flow diversion is short-lived.

As demonstrated by the SCTF and CCTF experiments, the conservatism of not considering multi-dimensional effects on core reflooding far out weighs the effects of any possible flow diversion caused by the difference in the design of the two assemblies. Within the hotter fuel assemblies, substantially more water is boiled and entrained by the reflooding process than within the average and colder assemblies. As the core fills, hydrostatic head imbalances are set up that lead to greater flow of water to the hottest assemblies and correspondingly less to the coldest assemblies. As a reasonable estimate, a measure of the flow imbalance is the radial power peak, whereby inlet velocities for the hot assembly 30 to 50 percent higher than those of the average channel can be expected. The result is a flattening of the cladding temperatures across the core radially and a reduction of the hot spot cladding temperatures of 300 to 500 F. The SCTF and CCTF experiments have demonstrated the improvements in hot

channel reflood cooling for radially peaked conditions. (The response to Question 2.a of the second round of questions on the RSG LOCA evaluation model, BAW-10168, presents a discussion of the effects observed in the experiments, or reference can be made to the appropriate experimental reports, references 28.1 and 28.2.) Thus, the consequence of simulation of all sources of flow diversion is a substantial decrease in cladding temperatures from the present calculational results.

As discussed, there is no flow diversion from one fuel assembly to another during reflooding with the calculational approach taken by the BWFC RSG LOCA evaluation model. The RSG LOCA model, in not considering the reflooding process at a level of modelling detail sufficient to evaluate reflood flow diversion, incorporates conservatism of far larger effect than those possible because of mixed core-induced flow diversion. Therefore, the evaluation of Appendix A, without the consideration of flow diversion, remains appropriate for the licensing of the transition cores.

- b. Clarify how the fuel loading pattern during mixed core operation affects the possibility of flow diversion from one type of bundle to the other.

Response: The actual fuel loading pattern for a reload is determined relatively late in the reload design process and cannot be readily predicted. It is reasonable to assume that the transition from a mostly OFA core to a mostly Mark-BW core will take place over three cycles with one third of the fuel replaced in each cycle. Even after the third cycle, it is likely that the core design will continue to utilize a few OFA assemblies for several years. Therefore, the LOCA calculations are not performed to an accuracy level that would require precise knowledge of the fuel loading pattern.

As explained in the response to Part (a) of this question, the consideration of flow diversion during reflooding as a mixed core consequence is not appropriate for calculations with current evaluation models. Therefore, the only effect of the loading pattern on the evaluation presented in Appendix A is the degree to which the mixed cores resistance is changed. The considerations of Appendix A bound the extremes of most resistive, all OFA, to least resistive, all Mark-BW. As the loading pattern and the number of Mark-BW assemblies in the core change, the reflooding effect will transition from a 2 percent decrease in flooding rate to no effect. During the first cycle, for example, the decrease should only be about 1.3 percent.

Repeating the conclusions from Part (a), the evaluation of reflooding at a level of accuracy that could properly treat flow diversion would result in cladding temperatures several hundred degrees lower than those predicted by present techniques. At that level of detail, however, care would be required to assure that both the fuel loading pattern and the skew in steady-state core flow assumed did not limit the core design. At present, the conservative one-dimensional reflood treatment is independent of individual fuel assembly placement and, within the constraints of the results presented in Appendix A, independent of cycle design.

- c. Clarify how the above two items effect cooling of the OFA and Mark-BW assemblies during reflood.

Response: As developed in the responses to the other parts of this question, the evaluation of the coolability of the mixed core configuration as presented in Appendix A of BAW-10174 is conservative and appropriate for plant licensing. Realistically, flow diversion within the

reflooding core would occur because of power distribution effects as well as for fuel assembly resistance differences. The results of the SCTF and CCTF experiments (See the response to Question 2.a of the second set of questions on the RSG LOCA evaluation model, BAW-10168, and references 28.1 and 20.2) show a substantially larger benefit from power distribution-induced flow diversion than the expected deficit from the fuel assembly resistance mismatch. Thus, although some small amount of fuel assembly-induced flow diversion, resulting in a small temperature increase, is expected during reflood, that increase would be imposed on a cladding temperature several hundred degrees cooler than the present licensing calculations. Therefore, the cooling considerations for the mixed core configuration as expressed in Appendix A of BAW-10174 remain applicable and appropriated for licensing.



References:

- 28.1 T. Iwamura, M. Osakabe, and Y. Sudo, "Effects of Radial Core Power Profile on Core Thermo-Hydraulic Behavior during Reflood Phase in PWR-LOCAs," Journal of Nuclear Science and Technology, 20[9], pp 743 - 751, September 1983.
- 28.2 H. Akimoto, T. Iguchi, and Y. Murao, "Core Radial Profile Effect on System and Core Cooling Behavior during Reflood Phase of PWR-LOCA with CCTF Data," Journal of Nuclear Science and Technology, 22[7], pp 538 - 550, July 1985.

29. Clarify if the purge system is used during power operation at Catawba and McGuire. For containment isolation in general and specifically for the purge system if it is used during power operation, provide the following information:

- a. Verify the actual containment isolation valve closure times are less than or equal to the values used in the FSAR analysis of the reduction in the containment pressure resulting from the partial loss of containment atmosphere during a LOCA for ECC backpressure determination. Also, verify the effect of the containment pressure transient during the LOCA on valve closure times was included in the analysis. See SRP Branch Technical Position CSB 6-4.
- b. If the purge system is used during power operation, verify the containment pressure analysis assumed the purge valves were initially open. See SRP Branch Technical Position CSB 6-1.

Response: As described in Technical Specification 3.6.1.9, there are three containment purge systems at Catawba:

Containment Purge (VP) System

Containment Air Release and Addition (VQ) System

Containment Hydrogen Purge (VY) System

As stated in this specification, only the second of these, the VQ System, may be used during Modes 1-4. The actual valve closure times for the subject valves, VQ-2 and VQ-3 or VQ-15 and VQ-16, are required to be less than 5 seconds, the maximum isolation time given in Technical Specification Table 3.6-2. As stated in Supplement 2 to the Catawba Nuclear Station Safety Evaluation Report, NUREG-0954, p.6-1, the staff had concluded that "the effect on containment pressure of air lost through open purge/vent lines is insignificant." Therefore, there was no explicit value assumed for valve closure time in the FSAR analysis. As stated in Item 1a of Catawba FSAR Table 9.5.10-1, the VQ valve "actuators will close the containment isolation valves assuming full containment pressure differential and resultant flow." Since the amount of air lost through these valves at the start of a LOCA is

insignificant, the containment pressure analysis makes no explicit assumption about the valves.

The containment purge system information given above is generally applicable to McGuire. The exceptions are:

- 1) The VP system is allowed to be used up to 250 hours per calendar year at McGuire. A limit on the use of this system is consistent with BTP CSB 6-4. In practice the system is not used during power operation since containment pressure is maintained with the VQ system.
- 2) The VQ (and VP) system maximum valve isolation time is 3 seconds, which is conservative with respect to the 5 second Catawba time.

30. There was no question number 30 in the NRC question set on the Duke Reload LOCA Analysis, BAW 10174, Revision 0. The following material documents the PCT impact on the LOCA Limits results, reported in Section 8 of BAW-10174, due to previously identified BEACH code errors, a change in grid design that impacts the BEACH input characterization of the grids, and the reduction of the metal-water reaction threshold temperature from 1500 F to 1000 F.

Response: BWFC described BEACH, Version 10.0, code errors at length in the response to question 5 of the NRC question set on the BEACH topical report, BAW-10166, Revision 1. In brief, the errors influenced the capability of the code to properly calculate gap heat transfer. Cladding temperatures in the vicinity of rupture were particularly affected. The errors were corrected in BEACH, Version 11.0, and that code version was validated against experimental data in response to question 5. The code updates were made subsequent to the release of the Duke Reload LOCA Analysis. The results presented in Section 8 of the Reload Analysis would, therefore, change, although not significantly, with implementation of the corrections. The results of reflood clad temperature evaluations of the LOCA Limits cases using the corrected code, BEACH, Version 11.0, are documented in this response.

In addition to the computer code error corrections noted above, the current results include the effects of updated grid parameter inputs. Since submittal of the Reload LOCA Analysis report, grid changes, influencing the characterization of flow blockage at grid locations, have been implemented. As a result of this change, grid blockage has been reduced from 55 percent to 49 percent for the current evaluation.

Benchmark re-evaluations, using Version 11.0, show that the code corrections result in reduced cladding temperatures and PCTs, while the grid parameter changes tend to increase clad temperatures. Lower grid blockage reduces droplet breakup and

steam cooling via interphase heat transfer. Grid convective effects associated with lower blockage also reduce heat transfer.

The results of the updated LOCA Limits calculations are tabulated in Table 30-1. They are compared directly to the results reported in BAW 10174. It is concluded that, in general, the revised evaluation results in an extended time-to-rupture and lower cladding temperatures. Both the rupture delay and cooler temperatures are a result of corrections in the gap conductance model implementation. The revised benchmark evaluations, included in the response to BEACH question 5, indicate similar results.

The 6.3-foot LOCA Limits case does not lend itself to a generalized conclusion. The location of the PCT in the revised evaluation changed from node 12 to node 14. This change is primarily due to the corrected gap model. After rupture occurs, the gas modeled within the gap is now realistically replaced with steam at the rupture location. Steam, having a lower thermal conductivity than the gap gas, causes a rapid decrease in the ruptured clad temperature. This phenomena can also be seen in the benchmark re-evaluation. Cooler steam and lower cladding temperatures at the rupture location will cause the adjacent clad temperature to be cooler as well. The PCT location is consequently transferred downstream to the next grid span and the PCT occurs at node 14 (in the revised case). Thus, the results of the re-evaluation of the 6.3-foot LOCA Limits case are decidedly more realistic than those reported in BAW-10174.

A change in metal-water reaction threshold temperature, from 1500 F to 1000 F, was implemented after completing the re-evaluation of the LOCA limits cases whose results are given in Table 30-1. The change in metal-water reaction methodology should not significantly alter the PCTs presented in Table 30-1, since the reaction is not a dominant energy contributor

below 1500 F. This was confirmed by a review of the 4.6-foot LOCA limits case, the elevation exhibiting the most severe local and whole core metal-water reaction percentages as well as the highest PCT, using a 1000 F metal-water reaction threshold temperature. The 4.6-foot review case was performed in response to question 13 of this question set regarding the effect of the threshold temperature on clad oxidation. The PCT results of the 1000 F threshold temperature case are compared to the 1500 F case, from Table 30-1, in Table 30-2. As expected, the PCTs differ by only 2 F. Figure 30-1 shows the clad temperature responses for the 1000 F threshold temperature case. In comparing Figure 30-1 with Figure 8-9 of BAW-10174, it is observed that the code, input, and methodology updates do not impact previously established cladding temperature trends. Thus, the results of the LOCA limits cases presented in Table 30-1 remain valid for the change in metal-water reaction methodology.

In conclusion, the revised LOCA limits calculations presented in Table 30-1 indicate that implementing the code corrections associated with DEACH, Version 11.0 and updating the grid input parameters does not significantly change the cladding temperature behavior given in BAW-10174. The maximum change in PCT is shown to be 31 F. The comparison in Table 30-2 using the most limiting, 4.6-foot, case shows that the updated metal-water reaction method has virtually no PCT impact; thus, the results in Table 30-1 are not perturbed. The PCT results shown in Table 30-1 and the response to question 13 all demonstrate that substantial margins to 10CFR50.46 criteria continue to exist subsequent to the revisions.



Table 30-1					
Case =	2.9' Peak	4.6' Peak	6.3' Peak	8.0' Peak	9.7' Peak
Parameter :					
Rupture Time, s	89.3 (81.8)	84.0 (74.4)	75.3 (67.6)	74.8 (73.8)	93.2 (84.4)
Rupture Node	8 (8)	8 (8)	11 (11)	14 (14)	15 (15)
PCT Time, s	282 (268)	240 (220)	338 (218)	227 (207)	220 (346)
PCT Node	11 (11)	11 (11)	14 (12)	12 (12)	14 (14)
PCT, ° F	1826 (1816)	1943 (1963)	1897 (1873)	1899 (1930)	1793 (1823)
Values in parentheses were generated with BEACH, Version 10, for Table 8-1 of BAW 10174, Revision 0, 1500 F metal-water reaction cut-off.					

Table 30-2		
Case =	4.6' Peak 1500 F Cut-Off	4.6' Peak 1000 F Cut-Off
Parameter :		
Rupture Time, s	84.0	83.2
Rupture Node	8	8
PCT Time, s	240	196
PCT Node	11	11
PCT, ° F	1943	1945
Values in table were generated with BEACH, Version 11		

FIGURE 30-1. LOCA LIMITS STUDY - 4.6-FOOT CASE CLAD TEMPERATURES.  
1000 F METAL-WATER FRICTION THRESHOLD TEMPERATURE

