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SUBJECT: Responds to NRC request to update submittal ltr dtd 950804 redifining repair boundary for parent tube behind HEJ sleeve

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December 20, 1996

AEP:NRC:1129M
10 CFR 50.90

Docket No: 50-315

U. S. Nuclear Regulatory Commission
ATTN: Document Control Desk
Washington, D. C. 20555

Gentlemen:

Donald C. Cook Nuclear Plant Unit 1
TECHNICAL SPECIFICATION CHANGE TO ALLOW FOR REPAIR OF HYBRID EXPANSION JOINT (HEJ) SLEEVED STEAM GENERATOR TUBES
REQUEST FOR ADDITIONAL INFORMATION (RAI)

REFERENCES

1. WCAP-14641, "HEJ Sleeved Tube Structural Integrity Criteria: 'ΔD' Diametral Interference at PTIs", April 1996

2. Safety Evaluation by the Office of Nuclear Reactor Regulation Relating to Amendment No. 128 to Facility Operating License No. DPR-43 (Kewaunee), dated September 25, 1996

The purpose of this letter is to respond to your staff's request to update our original submittal letter AEP:NRC:1129E dated August 4, 1995. Specifically, the attachment to this letter redefines the repair boundary for the parent tube behind the HEJ sleeve. We note that WCAP-14641 erroneously stated that our original proposed technical specification (T/S) change, contained in our letter AEP:NRC:1129E, was withdrawn. The letter has not been withdrawn. This submittal should be considered as a supplement to the original request.

Current T/S repair requirements apply to the parent tube adjacent to the hydraulically expanded region of the HEJ sleeve upper joint. An alternate plugging criterion is proposed which assesses the integrity of parent tube indications based on the degraded joint geometry, with reference to the specific location of the flaw. The alternate criterion will be referred to in this document as the "Delta-D" (ΔD) criterion, since the continued operability of the HEJ sleeved tube is based on the measured diameter difference, or diameter delta, between the sleeve peak hardroll diameter and the diameter of the sleeve adjacent to the parent tube flaw in the upper joint.

A detailed description of the proposed changes and our analysis concerning significant hazards considerations are included in attachment 1 to this letter. Attachment 2 contains marked up pages of the current T/Ss. Attachment 3 contains the proposed revised T/S pages. Attachment 4 contains a revised copy of the non-proprietary report, WCAP-14641, prepared by Westinghouse Electric Corporation describing the alternate plugging criterion referred to as ΔD.

A001 1/1

270008

We believe the proposed changes will not result in (1) a significant increase in the amounts, and no significant change in the types, of any effluent that may be released offsite, or (2) a significant increase in individual or cumulative occupational radiation exposure.

These proposed changes have been reviewed by the Plant Nuclear Safety Review Committee and the Nuclear Safety and Design Review Committee.

In compliance with the requirements of 10 CFR 50.91(b)(1), copies of this letter and its attachments have been transmitted to the Michigan Public Service Commission and to the Michigan Department of Public Health.

The T/S pages submitted with this letter will be impacted by the T/S page changes in our letters concerning 2.0 volt steam generator tube plugging criteria and technical specification changes to allow use of laser welded sleeves for steam generator tubes. The markups attached to this submittal do not account for any approval or incorporation of those other submittals but reflect T/S pages in their current state.

Sincerely,

E E Fitzpatrick

E. E. Fitzpatrick
Vice President

SWORN TO AND SUBSCRIBED BEFORE ME

THIS 20th DAY OF December 1996

Jan Watson
Notary Public

My Commission Expires: JAN WATSON
NOTARY PUBLIC, BERRIEN COUNTY, MI
MY COMMISSION EXPIRES FEB. 10, 1999

jmb

Attachments

cc: A. A. Blind
A. B. Beach
MDEQ - DW & RPD
NRC Resident Inspector
J. R. Padgett

ATTACHMENT 1 TO AEP:NRC:1129M

HEJ SLEEVED TUBE STRUCTURAL INTEGRITY LIMITS

DONALD C. COOK NUCLEAR PLANT UNIT 1

SIGNIFICANT HAZARDS CONSIDERATION ANALYSIS

I. DESCRIPTION OF THE CHANGES

The proposed additions are necessary to incorporate the "Delta-D" (ΔD) criterion. The specific additions are as follows.

1. T/S 4.4.5.2.g page 3/4 4-8

HEJ sleeved tubes determined to be operable by application of the Delta-D criterion (per 4.4.5.4.a.14) will be inspected in the upper HEJ region at each refueling outage to determine if new indications are present. Existing indications in these sleeved tubes which initiated the installation of a sleeve may be excluded from the requirements of 4.4.5.2.b.1.

2. T/S 4.4.5.4.a.6 page 3/4 4-10

This definition does not apply to HEJ sleeved tubes which may experience degradation in the upper HEJ hardroll lower transition region. Refer to specification 4.4.5.4.a.14 for the plugging limit applicable to these tubes.

3. T/S 4.4.5.4.a.14 page 3/4 4-11

a. HEJ sleeved tubes with circumferential indications located within the upper hardroll lower transition shall be inspected with a non-destructive examination (NDE) technique capable of measuring the sleeve ID difference between the sleeve hardroll peak diameter, and the sleeve ID at the elevation of the parent tube indication (PTI). If this diameter change is ≥ 0.003" (plus an allowance for NDE uncertainty), the tube may remain in service provided the faulted loop steam line break (SLB) leakage limit from all sources is not exceeded. A SLB allowance of 0.025 gpm shall be assumed for each sleeve tube left in service by application of this criterion, regardless of length or depth of the indication. For tubes where the measured diameter difference is > 0.013", SLB leakage can be neglected.

b. HEJ sleeved tubes with a sleeve ID difference of < 0.003" (plus an allowance for NDE uncertainty) between the sleeve ID hardroll peak diameter and sleeve ID at the elevation of the PTI shall be plugged or repaired prior to returning the steam generator to service.

c. HEJ sleeved tubes with axial indications located within the parent tube pressure boundary as defined on Figure B 3/4.4.5-1 shall be plugged or repaired prior to returning the steam generators to service.

d. HEJ sleeved tubes with parent tube indications located outside of the parent tube pressure boundary (below the upper hardroll region) as defined on Figure B 3/4.4.5-1 may remain in service.

4. Paragraph 3/4 4.5 page B 3/4 4-2b

Inspection experience at other plants with HEJ sleeved tubes has indicated that a potential exists for the sleeved tube to develop primarily circumferentially oriented degradation in the upper HEJ hardroll lower transition region. The pressure boundary for HEJ sleeves is shown on Figure B 3/4.4.5-1. The pressure boundary used to disposition parent tube indications (PTIs) detected in the upper joint of HEJ sleeved tubes is discussed in WCAP-14641, "HEJ Sleeved Tube Structural Integrity Criteria: ΔD Diametral Interference at PTIs," April 1996. The pressure boundary will allow PTIs located such that there is a minimum diameter change of 0.003" (plus an allowance for NDE uncertainty) between the peak diameter of the sleeve hardroll, and the diameter at the elevation of the PTI, to remain in service. The 0.003" interference lip is derived from structural and leakage testing. When inspecting and dispositioning the PTIs, the acceptance criterion will be adjusted to account for measurement uncertainties associated with the technique used to measure the relative change in ID sleeve diameters. During field application, the PTI elevation will be measured by comparing the diameter reported at the peak amplitude of the flaw, and the diameter at the center of the coil's field, and using the more conservative of the two diameters to perform the Delta-D determination. Application of the pressure boundary for HEJ sleeved tubes provides allowance for leakage in a faulted loop during a postulated steam line break (SLB) event. A SLB leakage of 0.025 gpm is assumed for each applicable indication. Steam line break leakage from all sources must be calculated to be < 8.4 gpm in the faulted loop. Maintenance of the 8.4 gpm limit ensures off-site doses will remain within a small fraction of the 10 CFR Part 100 guideline for a SLB.

Figure B 3/4.4.5-1 has also been added.

II. BACKGROUND

This evaluation applies to Cook Nuclear Plant unit 1, which utilizes Westinghouse Series 51 steam generators, and assesses the integrity of the tube bundle with tube eddy current inspection indications which may occur in the upper hybrid expansion joint (HEJ) region of mechanically sleeved tubes. Current technical specification (T/S) plugging requirements apply to the tube adjacent to the hydraulically expanded region of the upper joint and above. An alternate plugging criterion is proposed which assesses the integrity of these parent tube indications based on the degraded joint geometry, with reference to the specific location of the flaw. The alternate criterion will be referred to in this document as the ΔD criterion, since the continued operability of the sleeved tube is based on the measured diameter difference, or diameter delta, between the sleeve peak hardroll diameter and the diameter of the sleeve adjacent to the parent tube flaw.

The current Cook Nuclear Plant unit 1 40% imperfection depth tube plugging criteria does not take into account the inherent structural integrity of the HEJ configuration. Inclusion of this criterion involves an amendment to the T/Ss, and therefore, an evaluation per 10 CFR 50.92 is required.

III. REGULATORY BASIS

The guidelines for inservice inspection of steam generator tubes are contained in RG 1.83, revision 1. These guidelines include sample selection and size, inspection interval, personnel qualification, acceptance limits, and corrective measures. RG 1.121 provides the bases for plugging degraded steam generator tubes and uses safety factors consistent with section III of the ASME code.

Cook Nuclear Plant unit 1 T/Ss provide a limit on the amount of leakage allowable through the steam generator from the primary to secondary systems. When limited to the leakage permitted by the T/Ss, a crack or other through wall degradation would not be expected to result in a tube rupture situation. As part of the application of the voltage based plugging criteria for outer diameter stress corrosion cracking (ODSCC) at tube support plate (TSP) intersections, as described in GL 95-05, primary to secondary tube leakage following a postulated main steam line break event outside of containment but upstream of the main steam line isolation valves must not exceed the value calculated in support of the GL 95-05 application. All tube leakage sources therefore must be considered when direct steam release is modeled.

IV. EVALUATION

Tube/Sleeve Interaction and Postulated Loading Mechanisms

The fitup configuration of the HEJ sleeved tube assembly provides for a double wall condition with expected contact between the tube and sleeve in the hydraulically expanded regions during operation and positive metal-to-metal interaction between the tube and sleeve in the hardroll region that provides both structural and leakage integrity. The effect of the hardroll produces a radial preload between the tube and sleeve as well as creating a geometric discontinuity (transition shape) which effectively "locks" the tube to the sleeve.

The expected location of parent tube degradation is in the hardroll lower transition region of the upper joint. The upper portion of the hardroll lower transition is located approximately 2.75 inch from the top end of the installed sleeve. The installation of the sleeve is such that a 0.5 inch non-expanded sleeve length is provided at the top end, which transitions into a 4 inch long hydraulically expanded region. The hardroll is located approximately 1 inch down into the hydraulically expanded length. The hardroll region flat length is 1 inch long with 1/4 inch long upper and lower transition regions. Therefore, for the postulated HEJ sleeved tube to represent a tube rupture potential, the tube must become circumferentially separated in the hardroll lower transition region and be axially displaced along the sleeve by approximately 3 inches in order for sufficient flow area to be provided. The potential for this to occur is negligible since the radial preload between the tube and sleeve in the hardroll region plus the geometric

discontinuity in the transition provides sufficient restraint against axial displacement of the tube. For the tube to experience axial motion the tube must become completely separated at the flaw elevation and the applied end cap load must overcome the inherent integrity of the joint configuration. The inherent integrity within the joint is provided by the preload friction of the hardroll operation in addition to the interference of the tube material lip created by the transition shape, which requires the tube to be essentially extruded over the sleeve OD in the hardroll region. Protection against tube rupture is further provided by the design and configuration of the tube bundle. The maximum amount of axial displacement that a tube could experience along the sleeve using worst case fitup conditions at the TSPs and in the U-bend region and assuming frictionless tube-to-tube support plate interaction is calculated to be 1.1 inch. Therefore, it can be concluded that, due to the fitup configuration of the tube and sleeve, and based upon the design configuration of the tube bundle, postulated parent tube degradation in the hardroll lower transition region of a HEJ sleeved tube cannot result in tube rupture-type primary to secondary flow release rates.

The loading mechanism for the sleeved tube during operating conditions is developed by the primary to secondary pressure differential end cap loading. The product of the tube ID area and primary to secondary pressure differential results in a tensile load being applied to the tube, assuming that no fixity between the tube and TSP exists. Using established guidance of RG 1.121, in order to establish continued operability, the proposed criterion must provide for structural integrity such that the strength characteristics of the assembly equal or exceed the limiting loading defined by RG 1.121 to be 3 times the normal operating conditions primary to secondary pressure differential. Cook Nuclear Plant unit 1 can theoretically operate with a primary to secondary pressure differential of 1600 psi. At a pressure differential of 1600 psi, the limiting RG 1.121 end cap load used for analysis purposes is 2264 lb. The current primary to secondary pressure differential at full power, normal operating conditions at Cook Nuclear Plant unit 1 is approximately 1455 psi. Therefore, use of a primary to secondary pressure differential of 1600 psi for analysis purposes is conservative.

<u>Structural Integrity Characteristics And Test Program Results</u>

Per reference 1, a test program was performed in which Alloy 600 tube samples had Alloy 690 HEJ sleeves installed to typical HEJ field installation parameters for Westinghouse steam generators with 7/8" OD tubing. The tubes were then machined on the OD such that the tube was separated in the hardroll lower transition region. The diameter change between the tube OD at the hardroll peak diameter and machining location were measured. Additional testing showed that the tube OD diameter change was conservative or was consistent with the ID diameter change. The range of diameter change based on OD measurements was 3 to 6 mils for the test population. The samples were then heated in a clamshell type laboratory furnace to 600° F and tensile loaded in a tensile testing machine. A second order regression curve of the test data was developed and is contained in reference 1. Using the curve, a ΔD value of 3 mils provides for a peak axial load bearing capability of approximately 5200 lb force, while at a ΔD of 2 mils, the

expected peak load bearing capability is 4300 lb force. At a 0 mil ΔD value, the extrapolated peak load bearing capability is approximately 1750 lb force. Using the curve in reference 1 a ΔD value of approximately 0.4 mils will provide for axial restraint capability (assuming frictionless TSP intersections) equal to the limiting RG 1.121 end cap load of 2264 lb. This suggests that the friction force developed by the hardroll preload supplies sufficient axial restraint capability to balance the limiting RG 1.121 end cap loading and the axial restraint capability provided by the interference adds additional margin. Evaluation of load deflection curves for the tests indicate that slippage between the tube and sleeve did not occur below about 4000 lb load for all test samples.

Testing performed in 1994 can be used to validate the extrapolation of the ΔD structural limit for ΔD values less than the test population. In these tests, HEJ samples were prepared by separating the tube at various elevations within the hardroll region. The nominal hardroll flat length is 1 inch. Two samples with less than 1 inch of hardroll engagement were tested. Machining marks were clearly evident on the sleeve OD in the hardroll flat region. One sample with a remaining tube to sleeve hardroll flat length of approximately 0.8 inch exhibited a peak load of 625 lb force, and the second sample with a hardroll flat length of approximately 0.88 inch exhibited a peak load of 1760 lb. As machining of the tube on these samples resulted in less than a typical hardroll flat length of 1.0 inch, which would represent a less than 0 mil ΔD, the validity of the extrapolated curve for ΔD values less than 3 mils is justified.

The samples developed for the structural integrity testing are considered to be conservative representations of the field joints in that the test samples were purposely produced with rolldown, a result of the reverse rolling process. Rolldown results in an extended hardroll lower transition which provides for a shallower taper in the transition than expected using the field tooling. The field tooling provides axial restraint of the hardroll assembly thereby limiting the amount of rolldown experienced in the field. The more abrupt transition geometry produced by the field tooling should provide for added structural capability of the field produced HEJ sleeved tube assemblies compared to the test specimens. In 1995, HEJ sleeved tube sections were removed from another plant. The sleeved tube samples were tensile loaded to failure. Field NDE of these tubes indicated that the parent tube indication was nearly throughwall for 360°. The parent tube flaws however were not throughwall, and the ligament failure loads determined by tensile loading were in excess of 10,000 lb. Destructive examination indicated the flaw to be highly segmented, with 360° involvement, a peak flaw depth of 92% throughwall, with average macrocrack flaw depth of ~ 60%. In 1996, several more HEJ sleeved tube sections were also removed. Field NDE indicated 360° involvement. Upon examination, it could be seen that in one sleeved tube section the parent tube flaw extended for 360° and that the flaw was 100% throughwall over the entire circumference. Since the flaw was entirely throughwall, this sample most closely matched the laboratory test specimens. During tensile loading, the peak frictional load was developed at 0.2" of indicated machine motion with a corresponding peak resistive load of 5400 lb. The field NDE ΔD call for this tube was 3.1 mils. The peak resistive loads developed after

ligament failure for the remaining tensile loaded sleeved tube specimens showed peak frictional loads ranging from 4250 lb. to 5250 lb. The field NDE ΔD calls for these tubes ranged from 0.0 mils to 0.3 mils. The HEJ sleeved tube samples tested in 1996 were tested at hot (600°F) conditions. The results of these specimens indicate that the laboratory test samples are representative of the field sleeved tubes, and large margins are provided by the proposed criterion.

Leakage Assessment

The currently accepted leakage allowance of 0.025 gpm per indication less than 0.013 mils ΔD will continue to be applied. The basis for this value is discussed in Reference 1.

Previously submitted data regarding the lower HEJ indicates that the lower joint will remain leaktight during all operating and faulted plant conditions. Therefore, the application of the criteria will not introduce additional leakage other than the allotted value of 0.025 gpm per indication.

Strain gauge testing of HEJ sleeve specimens installed in tubes with fixed conditions at the TSPs (tube was welded to TSP) indicates that the tube far field stresses above the HEJ following installation of the sleeve are less than below the HEJ, suggesting that the most likely location for degradation to occur is below the hardroll flat in the hardroll lower transition. Therefore, rapid degradation of the hardroll upper transition is not expected and therefore not expected to contribute to the leakage allowance.

Eddy Current Uncertainty

The previously described 3 mil ΔD structural integrity limit has been licensed at another plant with steam generators like those at Cook Nuclear Plant unit 1 (reference 2). The eddy current uncertainty used for the application of the criterion at the other plant was specified to be 4 mils. This uncertainty was determined by measuring the ΔD using the OD diameter change as a guide. Due to the installed inside diameters of an HEJ sleeve, mechanical measurement devices could not be used to accurately measure the ΔD value for the ID. The shape and configuration of mechanical measurement devices would not permit an accurate ID measurement unless the sleeved tube specimen were separated and the diameters measured at the cut edge surface. Electric discharge machine (EDM) notches were used to simulate the tube flaw. The recorded eddy current values were then compared to the measured OD values and a statistical evaluation at a 99% confidence level established the eddy current uncertainty value at 4 mils. Later, some of the samples were sectioned by machining at the location of the EDM notch to better assess the accuracy of the eddy current process. Several samples became contaminated and the entire set could not be sectioned. Comparing the ΔD values determined from the actual ID measurements determined by sectioning with the values based on OD measurements, it is found that the eddy current determined ΔD values are closer to the actual ΔID values by about 1 mil for all sectioned samples. If the original OD based values are used and the statistical evaluation of error is taken at a 95% confidence level, an eddy current uncertainty of less than 3.0 mils can be justified. The extremely conservative analysis methodology

used in the field for the determination of the NDE based ΔD value is believed to result in the "undercalling" of the ΔD value, that is, the NDE based values are considered to be much smaller than the actual ΔD values.

For consistency purposes, Cook Nuclear Plant unit 1 will utilize a 4 mil eddy current diameter measurement uncertainty applied to the ΔD determination of HEJ sleeved tubes. As stated above, the eddy current uncertainty can be reduced to a value between 2.6 and 3.0 mils with no effective loss of safety margin. Significant margins are already built into the criterion by virtue of the fact that the test data base suggests that an actual ΔD value of 0.4 mils will provide axial restraint capabilities consistent with RG 1.121 tube integrity recommendations, but a 3 mil ΔD value is used. Further margin is built into the criterion since the expected axial restraint capabilities of packed TSP intersections are not included in the structural model.

Therefore, field application of the criterion will utilize an effective NDE based plugging requirement for HEJ sleeved tubes of 7.0 mils (3 mils structural component plus 4 mils eddy current uncertainty.) ΔD values obtained in the field of 7.0 mils or greater may remain in service.

V 50.92 ANALYSIS

Conformance of the proposed amendments to the standards for a determination of no significant hazard as defined in 10 CFR 50.92 (three factor test) is shown in the following.

1) Operation of Cook Nuclear Plant unit 1 in accordance with the proposed license amendment does not involve a significant increase in the probability or consequences of an accident previously evaluated.

   The HEJ sleeved tube structural integrity limits defined by this amendment provide for structural integrity consistent with the guidance of RG 1.121. Tube structural integrity consistent with the most limiting RG 1.121 loading is inherently provided by a measured ΔD of less than 1 mil, although the criterion specifies a minimum of 3 mils must be verified. The structural integrity characteristics of a postulated degraded parent tube with a 3 mil ΔD provides for axial restraint capability of more than double the most limiting RG 1.121 loading, which indicates that the postulated separated tube would not become axially displaced relative to the sleeve during any plant condition.

   Based on tube pull data from Cook Nuclear Plant and other plants it is expected that TSP intersections would provide a substantial axial restraint capability. This interaction is neglected in the analysis of the criterion, and provides for extra safety margin.

   Based on the destructive examination results for sections of HEJ sleeved tubes removed in 1994 from another plant, the parent tube flaw morphology is described as circumferentially oriented with multiple initiation sites. This segmented morphology indicates that the previously performed structural capability testing is conservative. Additional axial load bearing capability is provided by the segmented morphology

since end cap loading would be transmitted through the tube by the non-degraded ligaments of the segmented crack network, and tube separation therefore, is not likely or credible.

The consequences of any postulated failure of a sleeved tube to which the criteria has been applied would be bounded by the current steam generator tube rupture event discussed in the Cook Nuclear Plant Final Safety Analysis Report (FSAR). Axial displacement of any tube, sleeved or unsleeved, is bounded by approximately 1.1 inch. A tube which experiences axial displacement by this amount would be expected to exhibit a release rate well below the normal makeup capacity. In order for a HEJ sleeved tube to exhibit reactor coolant system release rates approaching the release rates assumed in the FSAR the tube must be displaced by approximately 3 inches. In order for the postulated separated tube to experience axial displacement of any magnitude, it must be assumed that the HEJ hardroll provides no structural benefit and that the tube-to-TSP interaction is frictionless.

Postulated primary to secondary leakage during a main steam line break event will be assessed against the limit of 8.4 gpm in the faulted loop, calculated as part of the voltage based plugging limit for tube support plate intersections. The total of all leakage sources must be shown to be less than this value.

Application of the 3 mil ΔD criterion (excluding eddy current uncertainty) does not change existing reactor coolant system flow conditions, therefore, existing LOCA analysis results will be unaffected. Plant response to design basis accidents for the current tube plugging and flow conditions are not affected by the repair process; no new tube diameter restriction is introduced.

2) The proposed license amendment does not create the possibility of a new or different kind of accident from any accident previously evaluated.

Application of the proposed 3 mil ΔD HEJ sleeved tube structural integrity criterion will not introduce significant or adverse changes to the plant design basis. The 3 mil ΔD criteria provides for structural integrity of the HEJ sleeved tube assembly which significantly exceeds the limiting RG 1.121 loading condition. Under these conditions neither a single nor a multiple tube rupture event is considered credible.

The general outline of the HEJ sleeve is unaffected, and the application of the proposed criterion does not change the sleeve configuration or size/shape. The application of the criterion also does not represent a potential to affect other plant components.

3) The proposed license amendment does not involve a significant reduction in a margin of safety.

The proposed criterion has been shown to provide structural integrity of the tube bundle consistent with the most limiting RG 1.121 tube integrity recommendations. In order for tube rupture to occur,

the degraded parent tube must experience a complete circumferential separation and be subsequently axially displaced by approximately 3 inches. The inherent structural integrity provided by the interference fit of the HEJ in addition to the axial restraint provided by tube support plate intersections above the HEJ provides for structural integrity far exceeding the RG 1.121 loading of 2264 lb. Even in the event that a degraded HEJ sleeved parent tube were to experience axial displacement, the maximum amount of displacement the tube could experience is bounded by 1.11 inch. Postulating that the tube were to become displaced by this amount, primary to secondary leakage would be limited to well less than the normal makeup capacity due to the proximity between the hydraulically expanded sleeve OD and tube ID.

Pulled HEJ sleeved tube samples from another plant with HEJ sleeved tubes indicate that the crack morphology is described as circumferentially oriented cracking with multiple initiation sites. This segmented morphology provides for additional structural margin not modeled in the testing program.

Existing flow equivalency calculations for the HEJ sleeved tubes will be unaffected by the application of the criterion.

VI CONCLUSION

Based on the preceding analysis it is concluded that operation of Cook Nuclear Plant unit 1 following the application of the 3 mil ΔD HEJ sleeved tube structural integrity limit does not increase the probability of an accident previously evaluated, create the possibility of a new or different kind of accident from any accident previously evaluated, or reduce any margins to plant safety. Therefore, the license amendment does not involve a significant hazards consideration as defined in 10 CFR 50.92.

Westinghouse Non-Proprietary Class 3

WCAP-14641

9612310170

HEJ Sleeved Tube Structural Integrity Criteria:

"ΔD" Diametral Interference at PTIs

W. K. Cullen
R. F. Keating

April 1996

WESTINGHOUSE ELECTRIC CORPORATION
NUCLEAR SERVICES DIVISION
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1.0 INTRODUCTION

In April 1994, crack-like indications were first detected in hybrid expansion joint (HEJ) sleeved tubes at the Kewaunee plant. Later in 1994, similar indications were detected at the Point Beach Unit 2 plant. Indications were again detected at Kewaunee, Point Beach Unit 2, and Zion 1 in 1995. Inspection of HEJ sleeved tubes at Cook Unit 1 in 1995 indicated no detectable degradation.

The indications detected to date have all been identified in the parent tube, with a majority of the indications located in the upper joint hardroll lower transition or below. The location of the upper joint hardroll lower transition is approximately 2.78 to 3.03 inches below the end of the sleeve.

In 1994 and 1995, eddy current examinations of this area detected a significant number of indications located below the HEJ hardroll yet still within the hydraulically expanded region of the joint. In accordance with the WCAP and Technical Specification requirements, these tubes were removed from service by plugging. Subsequent analyses, evaluations, and tests have shown that the geometric configuration of the sleeve-to-tube roll expansion and resultant material interaction continues to provide sufficient structural integrity and leakage resistance provided the sleeve inside diameter at the elevation of the indication in the parent tube is less than the nominal sleeve inside diameter in the roll expanded region. Nearly all of the sleeved tubes plugged meet this requirement, and therefore, provide for substantial structural integrity margin to existing regulatory guidance and leakage criteria.

The purpose of this report is to propose a redefinition of the repair boundary for the parent tube behind the sleeve. The redefinition of this portion of the repair boundary will provide for structural integrity characteristics consistent with draft Regulatory Guide 1.121, and leakage integrity consistent with recently published NRC guidance related to steam generator tube integrity and the Standard Review Plan. The revised repair boundary location is based on analytical evaluations, results of conservative testing programs, and results of destructive examinations of HEJ specimens removed from a Kewaunee SG. The culmination of these results support the conclusion that provided a minimum diametral interference of 0.003 inch (excluding eddy current uncertainty) between the peak sleeve ID in the roll expansion immediately above the hardroll lower transition and the sleeve ID adjacent to the parent tube indication (PTI), that structural integrity will be provided for all plant conditions and leakage resistance will be provided such that offsite doses following a postulated main steam line break outside of containment will remain within 10% of the Part 100 guidelines.

The WCAP reports which outline the qualification of the HEJ sleeving process identified the entire tube upper sleeve joint which is hydraulically expanded (4 inches) as part of the pressure boundary region of the tube. As the roll expanded region alone provides structural integrity to the joint, the hydraulically expanded region below the joint provides no additional benefit. The interference specification of 0.003 inch, excluding eddy current uncertainty, provides the necessary structural and leakage integrity aspects to assure safe operation of the sleeved tubes, and the plant.

2.0 SUMMARY OF PREVIOUSLY SUBMITTED LICENSE AMENDMENT REQUESTS ADDRESSING PARENT TUBE DEGRADATION IN HEJ SLEEVED TUBES

2.1 Kewaunee

Upon discovery of the crack-like indications at Kewaunee in April of 1994, a change of the Technical Specification plugging requirements for sleeved parent tubes was discussed with the NRC Staff. This discussion proposed that indications below the hardroll lower transition be permitted to remain in service and that indications in the hardroll lower transition of less than a maximum permitted angle be allowed to remain in service. Testing was performed at that time to justify allowing PTI's in the upper joint lower hydraulic transition to remain in service. The results of the testing program conclusively showed that PTIs at this elevation provided both structural and leakage integrity far exceeding published NRC guidance. Due to time constraints, a proposed license amendment request was not submitted to the NRC Staff.

2.2 Point Beach Unit 2

Following the original Kewaunee discovery, a testing program was performed by Westinghouse in support of a license amendment request for the Point Beach Unit 2 plant. This additional testing more accurately defined the beginning of cycle crack length which would provide for structural integrity at end of cycle conditions. The proposed criteria was based solely on plastic overload of the non-degraded tube ligaments due to the pressure developed end cap loads. The testing program established a bounding accident condition leak rate in addition to demonstrating large margins relative to the structural integrity (pullout resistance) provided by the HEJ itself. The structural integrity testing showed that nearly a 100% margin was provided by the limiting angle criteria. That is, the test developed peak load bearing capability of the HEJ was twice the calculated load bearing capability of the non-degraded ligament region. Test samples were prepared with 240°, 100% throughwall slits in the parent tube at the top of the hardroll transition region, or approximately 1 inch below the start of the hardroll flat length. In many cases, the actual peak load bearing capability of this configuration exceeded 8000 lbs, which is approximately 3.5 times the most limiting RG 1.121 end cap loading of 3 times normal operating

pressure differential. In the Fall of 1994, the Wisconsin Electric Power Company (WEPCo) formally submitted a Technical Specification change request to the NRC limiting beginning of cycle throughwall crack lengths of a maximum of 179° to remain in service.

The proposed TS change was subsequently denied by the NRC based on the conclusions documented in the Safety Evaluation Report prepared in response to the WEPCo request, dated January 11, 1995.

2.3 Cook Unit 1 Submittal

In August, 1995, a submittal proposed for the Cook Unit 1 plant revised the criterion to be analogous to the F* (F-star) tubesheet region criteria except this criterion applied to HEJ sleeve joints. The proposed criterion assumed that the tube experienced a complete circumferential separation at a location of 1.1" below the beginning of the hardroll region flat length. This request was withdrawn by the American Electric Power Company because no PTIs were detected in the HEJ sleeved tubes in the Cook 1 SGs.

2.4 Kewaunee

In October of 1995, Wisconsin Public Service proposed a TS change for the Kewaunee plant similar to the Cook Unit 1 plant. The results of that submittal are discussed in detail in the Section 3.

3.0 SUMMARY OF LATEST LICENSE AMENDMENT PROPOSAL

Subsequent discussions amongst the NRC Staff, Westinghouse, Zetec (eddy current probe vendor), Wisconsin Public Service, and American Electric Power had led to the identification of a need to revise the proposed HEJ repair boundary definition (i.e., the portion of tube behind the sleeve necessary for structural and leakage integrity) from a measure based on the nominal length of roll interaction to a measure of sleeve/tube diameter interference between the hardroll region and the elevation of the PTI. This is discussed below.

The latest license amendment proposal serves to address all previously identified NRC concerns.

NDE Uncertainty

NDE uncertainty is minimized since accurate measurement of crack angles and depths are not required. All detected degradation is assumed to be throughwall and extend for 360° around the tube circumference.

Structural Integrity

Issues of rolldown influence upon joint strength have been addressed through previously submitted and new information contained in this document. The effects of rolldown do not have a significant detrimental effect upon the joint structural integrity. A reexamination of the specimens used in the original Kewaunee testing of 1994, and the Point Beach testing of 1994 indicated that all samples had significant rolldown lengths, and should be conservative compared to the field produced sleeve/tube joints.

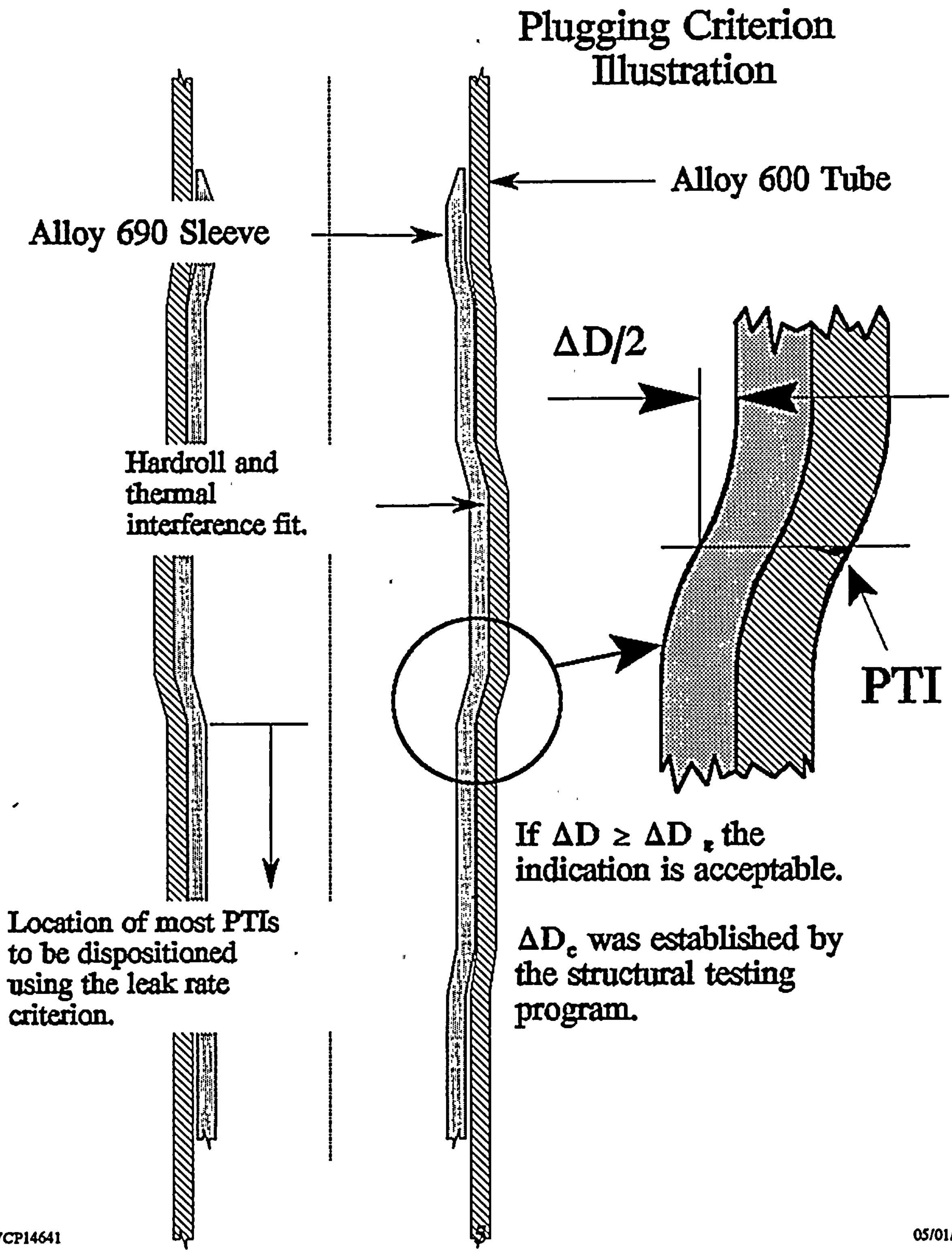
Based on these two key points being addressed, the criteria provides for structural integrity and leakage resistance. The criteria contains two key points:

1. The sleeve ID corresponding to the elevation of the crack in the parent tube must be a minimum of 0.003" less than the diameter at the maximum sleeve ID hardroll diameter.

2. Each sleeved tube permitted to remain in service through application of the revised pressure boundary criteria with a PTI elevation corresponding to a diameter difference between the peak hardroll diameter of 0.003 to 0.013 inch is assigned a bounding SLB leakage allowance of 0.025 gpm. This value is a conservative estimation of SLB leakage. The HEJ sleeved tube leakage allowance is added to the predicted leakage from all other sources, such as tubes left in service as a result of the application of the voltage based repair criteria for tube indications at TSP elevations (IPC), to verify that offsite dose does not exceed 10% of the 10 CFR Part 100 guidelines. For PTIs located below the hardroll such that the diametral difference, ΔD, between the peak hardroll diameter and sleeve ID at the PTI is greater than 0.013 inch, the SLB leakage may be neglected. The 0.025 gpm leakage allowance provides sufficient margin that any postulated leakage emanating from tubes with PTI ΔD values of greater than 0.013 inch will be accounted for.

The revised repair boundary is illustrated on Figure 3.1. The revised repair boundary is applicable to the HEJ sleeved tubes in domestic Model 51 steam generators.

FIGURE 3-1
REVISED HEJ SLEEVED TUBE REPAIR BOUNDARY for
PARENT TUBE INDICATIONS



4.0 EVALUATION AND DISCUSSION OF DATA

4.1 Previously Submitted Data

Introduction

In April of 1994, a testing program was performed to assess the structural integrity of hybrid expansion joint (HEJ) sleeved tubes. The intent of the test program was to develop information to support permitting sleeved tubes with parent tube indications (PTIs) below the upper hardroll joint to remain in service within the Kewaunee Nuclear Power Plant steam generators. Subsequent engineering work was performed over the next year and a half to address NRC questions associated with the original and subsequent information submittals. The discussion contained in this section is limited to disclosing information obtained from a recent reexamination of the specimens tested as part of the original program in 1994. The reexamination consisted of specimens with the tube completely separated by machining at various elevations in the hardroll lower transition. Alloy 600 tube specimens were used with Alloy 690 sleeve sections. Structural capability testing was performed at 600°F in a 55,000 lb maximum capacity tensile testing machine. After hydraulic expansion and roll expansion, the tubes of the tube/sleeve specimens were severed at various elevations by machining. The degradation simulants were conservative, even for 360° stress corrosion cracks, because the morphology of the degradation in the tube sections removed from Kewaunee consisted of non-coplanar, multi-ligament indications as opposed to single, throughwall flaws.

The data provided in this section presents again the results of structural testing programs performed in April 1994, along with a reevaluation of dimensional data from these specimens. Section 4.2 provides additional test data generated specifically to address recent NRC questions. This information is being provided to support a technical specification (TS) change to relocate the repair boundary based on a measured change in inside diameter of the sleeve between the peak hardroll diameter located immediately above the hardroll lower transition and the diameter of the sleeve adjacent to the PTI, which may be located in or below the transition. From the limited number of samples reexamined, it can be concluded that sufficient structural integrity is provided when the difference between the sleeve maximum hardroll diameter and the crack elevation diameter is greater than or equal to 0.003".

The specimens used in this program were all produced manually, that is, without the aid of tooling devices to limit rolldown, hence, *all* of the samples used in this program had significant rolldown lengths. It is noted that restraints were used to limit the downward motion of the roll expander tooling thrust collar in the field installation tooling. By limiting the downward motion of the thrust collar, the roller cage itself is restrained from axial motion, and the length of the

rolldown area is minimized. The information presented below also supports the conclusion that rolldown experienced in the field will not detrimentally influence joint structural integrity.

In 1994, structural integrity testing was performed with crack simulants (throughwall machining of tube over 360°) at three elevations within or below the hardroll to hydraulic expansion transition using non-conditioned, non-honed, HEJ laboratory prepared specimens. A summary of the results is as follows:

(1) For four specimens separated below the bottom of the hardroll lower transition the structural integrity of the joint exceeded the ultimate load capacity of the sleeves of ~8000 lbs, or about 11 times the normal operating condition pressure difference, ΔP, end cap load.

(2) Two specimens (#5 and #17) separated at a hardroll lower transition length of 1/4" had peak pull force loads of 4100 lbs and 5400 lbs, respectively, or about 5.6 to 7.4 times the normal operating condition pressure difference end cap load.

(3) Two specimens believed to be separated at the top of the hardroll to hydraulic expansion transition, i.e., the bottom of the hardroll flat length with no interference *lip* present, had peak pull force loads of 625 lbs and 1760 lbs.

The specimens believed to be separated at the top of the hardroll lower transition were dimensionally examined. The findings prompted further examination of all of the samples and an evaluation of the pertinence of the data to the proposed criteria. The findings are summarized in the following paragraphs.

A. Samples separated at the top of the hardroll lower transition.

The post-pull testing flat length of the roll expanded region of the tube was measured using a precision machinists scale. The measured lengths were approximately 0.8 and 0.88 inch for the two samples, thus, the slit was machined well *above* the hardroll lower transition. This is substantiated by evidence of cutting tool marks on the sleeve OD in the roll expanded region and their relative position with regard to the beginning of the hardroll region flat length. The low pull forces would not have resulted in necking or significant deformation of the tube such that a reasonable accurate measure of the hardroll flat length could not have been performed. Not only was no interference lip present, but the full hardroll length was also not present. Therefore, the structural integrity test results for these two specimens should only be considered as *a* lower bound, but not a greatest lower bound, for sleeved tubes with circumferential cracks at the top of the hardroll lower transition.

B. Samples separated at a hardroll lower transition length of 1/4 inch.

The reexamination of these specimens revealed that they were separated at the specified distance of 1.5" below the beginning of the hardroll upper transition (the point when the tube hydraulically expanded OD above the rolled region began to change), or approximately 1.2" below the bottom of the HRUT. However, the initial assumption that the samples had typical hardroll lower transition geometries, i.e., a transition length of ~0.25" inch, was not verified. It was found that the samples had significant rolldown lengths as evidenced by the dimensional information provided in Table 4-1.

From the Table 4-1 information, the *post-pull diameter* difference between the sleeve ID in the hardroll versus the sleeve ID adjacent to the cut in the tube was approximately 0.005" for sample 17 and 0.002" for sample 5. Both of these specimens exhibited axial strength (4100 and 5475 lbs) well in excess of the RG 1.121 guideline end cap load caused by a pressure difference of $3\Delta P_{NOp}$, i.e., 2178 lbs for a pressure difference of 1540 psi. Moreover, Table 4-1 gives an indication of the extent of the rolldown experienced by these samples. Figure 4-1 provides a geometric representation of the profile of the hardroll joint and the diameters listed in Table 4-1. The "normal" (i.e., no rolldown) hardroll lower transition is approximately 0.25". At 1.75" below the beginning of the hardroll flat length, the sleeve ID was not yet reduced to the hydraulically expanded diameter above the hardroll, indicating that this area was still being affected by rolldown during the fabrication of the test specimens. These results imply that the length of the transition is of secondary importance in determining the strength of the joint, i.e., the amount of rolldown is not significant, only the magnitude of the diametral interference. As the dimensional information presented above was taken following the pull, it is unclear if the diameters were affected due to necking during the tensile loading. For these two samples, the expected load to introduce yielding of the sleeve is approximately 4500 lbs at 600° F. Since the recorded peak load was 5475 lbs, it is probable that the sleeve experienced some necking in this region and the validity of the 0.005 inch diameter difference is questionable.

C. Samples separated at below the bottom of the hardroll lower transition

All samples separated at this elevation had a load bearing capability greater than the ultimate strength of the sleeve itself. The sleeves failed in tension at ~8000 lbs.

D. Slip Distance and Leak Rate Information

The force-deflection curves from all of the samples were recovered and re-examined. At 2500 lbs, sample 4, which failed in the sleeve, had a total indicated motion of approximately 0.070 inch. Samples 5 and 17 had indicated displacements of 0.090 and 0.110 inch at a load of 2500

lbs, leading to the conclusion that the amount of slippage experienced at a SLB end cap load of 1207 lbs would be insignificant. Total indicated testing machine motion includes slippage due to seating of the gripper jaws, elasticity of the tube and sleeve, and elasticity of the test equipment. Previous tube tensile loading experience suggests that when the total system elasticities and displacements are considered, relative motion between the tube and sleeve in the HEJ would not occur at the SLB ΔP end cap. Therefore, it can be concluded that whether the tube is separated *within or below* the hardroll lower transition, no relative motion between the tube and sleeve would occur. Moreover, data contained in WCAP-14157 Addendum 1 presents structural and leakage information for HEJ sleeved tube specimens with simulated cracking.

Two samples using Alloy 600 sleeves and Alloy 600 tubes were leak tested. These tubes were also circumferentially separated by machining. The measured distance from the beginning of the hardroll flat length referenced on the tube OD to the cut was ~1.16". Samples were pressurized to an differential pressure of 2560 psi at 600°F. The measured leak rate reported to be 0.0001 gpm. Pre- and post-leak test overall sample length measurements indicated that no relative motion between the sleeve and tube had occurred. The results of these tests indicate that the internal pressurization effects also add to the structural characteristics of the joint. These effects were not included in the previously discussed structural integrity testing.

4.2 Discussion of 1996 Test Data

Tensile testing of samples was performed in a tensile testing machine at the Westinghouse Science and Technology Center, Churchill, PA. Outside diameter gripper jaws were used to secure the samples to the testing machine. Total crosshead travel was recorded due to the fact that the furnace prohibited application of extensometers directly to the cut elevation. Included in the total machine travel are errors due to slippage of the gripping jaws and elasticity of the test specimen. Elasticity of the test specimen is approximately 0.020 inch at a load of 3000 lbs, while slippage in the jaws is more significant. The design of the gripper includes a "C" shaped outer collar with self-adjusting jaws. As a load is applied the jaws slip relative to the collar and cause an indentation into the tube or sleeve wall as the load continues to escalate. Previous testing has shown that at approximately 2625 lbs load, jaw slippage could be as large as 0.06 inch per jaw, resulting in total jaw slippage error of up to 0.12 inch at the $3\Delta P$ end cap load. In the case of the non-conditioned tube samples, the total machine motion exceeded 1.7 inches when the load dropped to zero while the engagement length (hardroll flat region plus transition length) between the tube and sleeve was approximately 1.12 to 1.19 inch.

4.2.1 Conditioned Tube Samples

This series of tests performed in 1996 were intended to justify the proposed ΔD repair boundary criteria. Tube and sleeve specimens were prepared using Alloy 600 tubes and Alloy 690 sleeves. The tubes were "conditioned" by exposure to primary water at elevated temperatures in an autoclave to apply an oxide film to the tubes. The ID of the conditioned tubes were honed prior to sleeve installation, similar to the field installations. Sleeve installation used computer controlled hydraulic expansion and manual hardrolling. Due to size limitations of the autoclave used for conditioning the tube samples were limited to 12" in length. In order to facilitate tensile testing in combination with the instrument furnace used to heat the samples, an overall specimen length of 19" was required for the sleeved tube specimens. The specimens were configured so that ~6.5" of tube extended above the sleeve and ~6.5" of sleeve extended beyond the tube at the bottom. A spacer tube section was utilized to fill this length difference below the tube to accommodate rolling. The spacer tube and sleeve were tack roll expanded at the sleeve bottom end and the two tube sections were clamped together during simulated upper roll expansion. After rolling it was found the samples experienced greater than anticipated rolldown lengths. Having rolldown in the specimens is conservative, since the shallower angle of the lip of tube material in a rolldown condition theoretically requires less force for deformation than a steeply angled or prototypic lip. After sleeve installation, the tubes were circumferentially slit to depths of 40% to 60% throughwall for eddy current diameter verification (performed by Zetec). The tubes were then separated throughwall by machining at various locations to support the proposed criteria, subjected to a second conditioning and then tensile tested. The tubes were slit with a 0.07 inch wide tool, resulting in an extremely conservative representation of the crack. The tube outside diameter differences from the midpoint of the hardroll to the tube cut location ranged from 0.002 to 0.005 inch, while the OD differences between the hardroll maximum diameter located at the top of the hardroll lower transition and tube cut location ranged from 0.003 to 0.006 inch. Selection of the slit locations was done prior to having results of the eddy current testing and establishment of the ΔD being specified between the hardroll *peak* diameter and PTI elevation. Based on examination of supplemental samples used for the wall thinning correlation, the variance in wall thinning is bounded by 0.001 inch, and may be ignored when estimating the sleeve ID adjacent to the cut (see Section 4.4).

For 4 of the samples the first slip load was greater than the 3ΔP end cap load while all samples had first slip load values exceeding the 1.4 times SLB ΔP end cap load. The total machine displacements at the 3ΔP end cap load were examined for the samples which had first slip loads less than the 3ΔP end cap load. When the total system slippage errors are considered, the relative displacement between the samples was determined to be less than 0.02 inch at the 3ΔP end cap load. The load escalation profile indicates that ratcheting of the joint (indicating slippage between the tube and sleeve in the HEJ with galling at the interface) occurs at about

4000 lbs resistive load. The load path followed a smooth curve up to this load point. The ratcheting load was consistent (3600 to 4000 lbs for all samples), indicating that the hardroll to hydraulic ΔD and hardroll peak diameter to PTI ΔD has a negligible impact upon this point in the load path. A summary of the peak loads, which ranged from 5200 to 6800 lbs, and diameter differences are furnished in Table 4-2. Physical examination of the specimens after the testing showed that a significant amount of galling occurs on the sleeve OD when the parent tube is pulled over the joint. A minimal amount of sleeve OD surface disruption is evidenced in the transition region, while the extent of sleeve OD surface disruption is much more prevalent in the roll expanded region, leading to the conclusion that the full hardroll length carries the vast majority of the load, and that the ΔD between the peak hardroll diameter and diameter adjacent to the PTI may affect the first slip load. An example of the force vs. deflection curves for several conditioned tubes are provided in Figures 4-2a thru 4.2d. Of interest in these curves are that the load at which ratcheting, or slippage with galling in the joint occurs at about the same for all samples, regardless of PTI ΔD or hardroll to hydraulic ΔD. Figure 4-3 is a graphical representation of the peak load vs. diametral interference for the samples tested. Figure 4-3 indicates the second order regression through the conditioned tube data. Interestingly, the peak load of 4100 lbs for the non-conditioned tube with a ΔD of 0.002" (discussed in Section 4.1.B) lies almost directly on the curve. The peak load of 5475 lbs for the non-conditioned tube with a ΔD of 0.005" lies slightly below the regression curve. Since the peak load for this sample exceeded the yield load of the sleeve at 600^0 F, the validity of the 0.005" value is questionable due to the load experienced by the sample and is addressed in Section 4.1.B. Figure 4-4 plots the first slip loads against the diametral interferences, and Figure 4-5 plots peak load vs. hardroll to hydraulically expanded ΔOD difference to show graphically that this parameter does not influence HEJ peak load capability.

Also of interest is the fact that galling was evidenced between the tube and sleeve at the upper edge of the hydraulically expanded region. The eddy current profiles and dimensional evaluation of the hydraulically expanded region shows that a slightly larger diameter is found at the ends of the hydraulic expansion. The thermal expansion characteristics of Alloy 690 suggest that the joint will become tighter at elevated temperatures, therefore, the evidence of galling at the top of the hydraulically expanded region simply validates this phenomenon. This phenomenon contributes to the integrity of the 0.003" ΔD repair boundary.

4.2.2 Non-Conditioned Tube Samples

Several non-conditioned tube samples were also used for preliminary force vs. displacement development. The tube lengths used for the samples used were 19" long, therefore the 6.5" spacers required for the conditioned samples were not required. The tubes and sleeves were tack roll expanded at the bottom end into a carbon steel collar prior to upper hardrolling. Unlike the

conditioned samples and the field installation, the non-conditioned samples were not honed. The "scuffing" of the tube ID surface due to honing appeared to somewhat increase the first slip values for the conditioned tube samples. Computer controlled expansion was used for the hydraulic expansion and these samples were prepared with similar roll expansion diameters as the conditioned samples. Similar ΔD values as the conditioned samples were used. A summary of the peak loads and diameter differences for these non-conditioned, non-honed samples are furnished in Table 4-3.

For the non-conditioned, non-honed samples NA-1 thru NA-4, the $3\Delta P$ end cap load was achieved at total machine travel of 0.13 to 0.15 inch. When the allowances for specimen elasticity and jaw slippage are included, the $3\Delta P$ end cap load resistance of the samples was likely to have been developed in approximately 0.01 to 0.03 inch of specimen relative displacement. The furnace was opened on several samples at the point when first slip was evidenced. No relative motion between the tube and sleeve could be visibly detected at that point since the slit width did not appear to widened. Relative motion would have been evidenced by scratching or surface galling as the tube is being pulled over the sleeve. Therefore, the first slip condition very well may represent the point of first relative motion between the tube and sleeve, and not separation of the tube at the location of the crack(s).

Two non-conditioned samples, NAFT-2 and NAFT-3 were tested with partially throughwall, 360°, 0.070 inch wide slits. The slit depth for NAFT-2 was 0.042 inch, or 84% throughwall, and the slit depth for NAFT-3 was 0.043 inch, or 86% throughwall. Full length tube sections were used for all of the non-conditioned specimens, that is, at the bottom of the sample the tube end and sleeve end were consistent, while at the top the tube extended ~6 inches past the end of the sleeve to facilitate pulling. At the bottom end the tube/sleeve were rolled into tubesheet collars. NAFT-2 had the tube and sleeve separated immediately above the collar with the tube only then separated several inches above the cut end of the sleeve so that this sample could be pulled as all of the other samples. NAFT-3 was produced with the tube/sleeve remaining within the collar, thereby simulating the actual tube condition within the steam generator. The configuration of sample NAFT-3 resulted in the entire end cap load being applied to the non-degraded ligament. This sample was intended to show that if it is postulated that a partially intact tube ligament fails in tension during loading that the interaction of the tube and sleeve as the tube is pulled over the sleeve is consistent with the other samples. The configuration of all other samples isolated the strength characteristics of the HEJ through the configuration of the samples. In NAFT-3, the HEJ became loaded only after the non-degraded tube ligament was separated.

NAFT-2 exhibited a first slip of 2000 lbs, which is consistent with the other samples, however the non-degraded ligament did not fail until 7500 lbs load. This was confirmed by opening the

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furnace and examining the sample immediately after the load reduction. No galling marks could be seen on the sleeve OD and there was no apparent extension of the cut. As the tube was pulled over the sleeve the tube material stretched, but remained intact until the 7500 lbs load. The bridging of tube material across the slit region produced a condition almost similar to the case where the tubes were separated by machining below the hardroll lower transition. This bridging of tube material across the simulated crack resulted in a condition such that the entire tube transition length interacted with the sleeve.

NAFT-3 failed in the ligament at 1925 lbs. Again this was confirmed by examination immediately after the first change in the load curve. The calculated limit load for the remaining ligament using the expected material ultimate strength adjusted for the 600° F test temperature is ~1700 lbs. No first slip condition was detected since there was no relative motion between the tube and sleeve until ligament separation. The load continued to escalate with no joint slippage or ratcheting until about 4000 lbs, which again is consistent with the other samples. The test of NAFT-3 was halted at a load of 5700 lbs applied to the HEJ since the sample was experiencing large displacements. The tube and sleeve had begun to slip within the tubesheet collar. At the load of 5700 lbs, the amount of relative motion between the tube and sleeve, as evidenced by scratching of the sleeve OD, was estimated to be approximately 0.06 to 0.09 inch. For tubes within the steam generator which have crack morphologies consistent with the pulled tubes, the eccentric crack pattern would be expected to lead to the bending lockup phenomenon discussed in WCAP-14157 Addendum 1. This bending lockup phenomenon was shown to enhance the structural integrity of the joint such that a non-degraded ligament calculated overload capacity equal to the 3ΔP end cap load was increased by a factor of 2.

4.3 Tube Pull Results

In April of 1995 three HEJ sleeved tube samples, R2 C32, R2 C54, and R2 C61, were removed from S/G B. These tubes were cut immediately above the top of tubesheet and several inches below the first TSP then removed from the steam generator from the secondary side so that the HEJ was not damaged.

As the intent of the HEJ sleeved tube removal was to examine the parent tube corrosion, R2 C32 and R2 C54 were tensile loaded to open the parent tube circumferential crack faces. The tube and sleeve were welded together at the bottom while the tube alone was loaded at the top. Tensile loading in this condition simulated the actual condition within the steam generator. Load vs. deflection curves were developed for both specimens. Testing was performed at room temperature conditions. Peak loads developed at the failure point of the non-degraded ligament were 10,359 lbs for R2 C32 and 10,700 lbs for R2 C54. Peak frictional loads developed after ligament failure were 2800 lbs and 4000 lbs, respectively. The crack elevation for R2 C32 was

at the top of the hardroll lower transition while the crack elevation in R2 C54 was in the approximate midpoint of the hardroll lower transition.

For each of the pulled tubes tensile loaded, the ligament failure loads and the peak frictional loads following ligament failure exceeded the most limiting RG 1.121 loading. Destructive examination indicated 360° cracking with minimum, average and peak crack depths of 5% TW, 61% TW and 92% for R2 C32, and 4% TW, 60% and 92% for R2 C54. The crack morphology was found to be segmented, with 21 and 19 ligaments respectively, separating the individual crack initiation sites for each tube. All corrosion was ID initiated and the macrocrack network was not found in a single plane.

4.4 Process Verification Samples

The samples used for verification of the structural integrity of the HEJ could not be measured in the hardroll ID region of the sleeve since the unexpanded diameter of the sleeve would not permit an ID micrometer in the 0.700 to 0.800 inch inspection range to pass. Therefore, process verification samples were used to develop a correlation between the measured OD and expected ID of the samples in the lower transition region. Process verification samples were hydraulically expanded, hardrolled, dimensioned and then sectioned. Tube/sleeve wall thickness measurements were taken at the cut face in the hardroll midpoint and at approximately 0.25 inch into the lower transition. From the examination of the wall thickness measurements, the maximum difference in wall thickness in the middle of the hardrolled region to approximately 0.25 inch into the lower transition was approximately 0.001 inch. The wall thicknesses at each elevation are likely to be consistent. At a ΔD value of 0.003", the distance into the transition should be less than 1/8 inch for the field tubes. Since the rollers are 1.5 inch long with a 1 inch long (nominal) flat length and 1/4 inch end tapers, the sleeve ID is in intimate contact with the rollers during the expansion at the ΔD = 0.003" location, and therefore, the wall thickness at this point should be consistent with the hardroll midpoint as the tube and sleeve at the ΔD = 0.003 inch value are in direct contact with the rollers during the roll expansion process. Therefore, for the samples used for structural integrity testing, the sleeve inside diameter differences at the cut location would be expected to be equal to the OD diameter differences measured at the cut locations on the structural integrity samples since the rolls were contacting the sleeve ID during the entire roll expansion process. At 1/8 inch into the taper on the bottom end of the roller the RADIUS change is approximately 0.010". As the torque input to expand a sleeve in air is small compared to the field procedure torque input, the rolls should be equally loaded on a unit length basis over the contact length between the sleeve ID and the rolls. As a conservative measure, a 0.001" uncertainty "factor" should be included in determination of overall system uncertainties (including NDE measurement uncertainty). It should be further

noted that the test data plotted in Figure 4-3 indcates sufficient structural integrity for actual ΔDs of a minimum of 0.002".

Based on these samples there was evidence of wall thinning relative to the original thickness of the tube/sleeve, due to the hardrolling. The amount of thinning was determined by subtracting the sectioned hardroll midpoint ID from the hardroll OD and subsequently dividing by two. The amount of wall thinning was found to depend on the size of the roll expanded sleeve ID. For sleeve roll expanded IDs of about 0.730 inch, the difference between original, unexpanded tube and sleeve total wall thickness to post rolled total sleeve and tube wall thickness was about 0.0035 inch. For roll expanded sleeve IDs of about 0.720 inch, the difference between original and rolled wall thicknesses was about 0.0025 inch, while the difference between original and rolled wall thicknesses for roll expanded sleeve IDs of about 0.715 inch was about 0.0016 inch. The average measured wall thinning by constant area expansion due to the hydraulic expansion process alone was found to be 0.0008 inch.

The hardroller design uses 4 individual roller "pins" held in place by a roller "cage". As the tapered center mandrel interacts with the rolls the mandrel is drawn upward by the rolls and the diameter is thereby increased. The use of 4 rolls provides for better concentricity control due to the rolling process. As all 4 rolls are pulled downward during the reverse roll or removal operation, the diameter in the lower transition should be similarly concentric with the roll flat length region as the rolls are spinning in reverse and the cage housing the rolls is similarly spinning in reverse.

4.5 Leakage Allowance

Previously submitted data can be revisited in order to establish a steam line break leakage allowance. In previous submittals leakage data for 240° throughwall slits located at the top of the hardroll lower transition were used to develop a SLB leakage allowance. Using data from WCAPs 14157 and 14157 Addendum 1, a SLB leakage allowance of 0.025 gpm per tube left in service due to application of the proposed criteria is therefore developed.

In WCAP-14157 HEJ samples were leak tested at 600° F. Crack simulation was achieved by completely machining away the tube at the listed elevation. For tubes completely machined away at the bottom of the hardroll lower transition and tube OD of 0.896 inch, which corresponds to a sleeve ID of 0.721 inch and ΔD (between the peak hardroll diameter and the elevation of the cut in the tube simulating the crack) of about 0.021 inch, average and peak leakage values of 0.0012 gpm and 0.0032 gpm for normal operation and 0.0025 and 0.0083 gpm for SLB conditions were recorded. For tubes completely machined away at the geometric inflection of the hardroll transition (listed value; validity of the actual cut location cannot be

determined), or at approximately 1/3 to 1/2 of the transition length, average and peak leakage values of 0.0008 and 0.0016 gpm for normal operation and 0.008 and 0.016 gpm for SLB conditions were recorded. As the average leak rate for samples with a full lower transition are 1/10 th of the applied per tube bounding leak rate allowance, the conservatism in the 0.025 gpm per indication bounding allowance should provide for adequate leakage estimation.

WCAP-14157 Addendum 1 presents structural and leakage test results for tubes with 240° throughwall slits located at the top of the hardroll lower transition using Alloy 690 sleeves Dimensional data presented in WCAP-14157 Addendum 1 indicates that most of the samples had a slightly larger diameter immediately above the slit compared to the hardroll midpoint, Therefore, these samples had an expected ΔD (between hardroll peak diameter and elevation of cut in tube) of 0.000 to 0.001 inch. Both nominally sized samples (0.723 hardroll ID) and small sized samples (0.714 inch hardroll ID) were used. The average and peak leakage values for the slit specimens at SLB conditions was 0.0063 gpm and 0.016 gpm, respectively. Of interest here is that the average and peak values for the case of a 0.020 inch wide, 240° throughwall slit and completely separated tube at the geometric inflection point of the hardroll lower transition were essentially equal. Further conservatism is provided through the use of 0.020 inch wide throughwall slit specimens as opposed to stress corrosion cracks. The flow area of the 240°, 0.020" wide slits is 0.042 in^2, and is a huge flow area compared to the actual crack opening area for a stress corrosion crack.

Two samples using Alloy 600 sleeves were completely machined away at an elevation of approximately 1.16 inch below the top of the hardroll flat, or, at about a ΔD of 0.008 inch using the 1996 test specimens as a guide. The SLB leakage for these specimens was 0.00001 gpm.

Another sample (#023) originally used for structural integrity testing was leak tested after tensile loading to ligament failure. This sample was one used for "zero friction" tests, and was configured such that the tube was tensile loaded at each end. A typical upper HEJ was produced while the tube and sleeve had no mechanical interaction at the bottom of the sample. The tube had a 240° throughwall slit located at the top of the hardroll lower transition. The sample was intended to result in tube ligament failure without introducing HEJ slippage or galling, in an attempt to show the benefit provided by the bending lockup phenomena evidenced in the 240° throughwall slit testing. The tube failed in tension at 5150 lbs force. Following separation, sample 023 was leak tested. The SLB leak rate was found to be 0.009 gpm.

From the slitted leakage samples and circumferentially separated leakage samples, the average SLB leak rate was found to 0.0063 gpm, which is 4 times less than the proposed bounding, per tube leakage allowance of 0.025 gpm per tube. The 0.025 gpm per tube allowance is 40% greater than the largest measured leakage test data point.

Table 4-1: 1994 HEJ Sleeved Tube Samples 5 and 17
Non-conditioned, Non-honed Tubes
Dimensional Characterization

	Specimen Inside Diameter Measurements (inch)				
Sample	HE Above HR	HR Midpoint	Transition at Tube Cut	~1.5" Below Top of HR	~1.75" Below Top of HR
5	0.704	0.724/0.724	0.722/0.723	0.717/0.717	0.710/0.710
17	0.704	0.725/0.725	0.720/0.720	0.712/0.713	0.707/0.706

Note: All diameters except the HE above the HR were measured at two locations which were 90° apart.

Table 4-2: Peak Load versus ΔD Values for Varying Hardroll to Hydraulic Expansion Diameters, Conditioned Samples						
Sample	First Slip Load (lbs)	Peak Load (lbs)	Sleeve HR ID (inch)	HR/HE ΔD (inch)	HR/Cut ΔD (inch)	TOF[1] to Cut (inch)
ST1	1880	5200	0.721	0.0230	0.003	1.06
ST2 [2]	1800	5900	0.721	0.0176	0.005	1.12
ST3	3100	6100	0.712	0.0090	0.006	1.06
ST4 [2]	2520	6600	0.731	0.0275	0.005	1.12
ST5 [2]	2120	6200	0.727	0.0225	0.006	1.12
ST6	3280	6800	0.714	0.0121	0.006	1.12
ST8	not tested	not tested	0.728	0.0244	0.005	1.06
ST10	2540	5800	0.715	0.0173	0.004	1.06

Notes:

(1) The term TOF refers to "top of hardroll flat" and is a measurement of the distance from the top or beginning of the hardroll flat length to the cut location. The top of the flat is consistent with the peak diameter measurement at the top of the hardroll.

(2) Roller torqued out prematurely due to bad bearing in roller assembly. Field installation would have rerolled tube. Test samples not rerolled to provide conservative points for structural evaluation.

Table 4-3: Peak Load vs. ΔD Values for Non-Conditioned HEJ Specimen Tensile Tests						
Sample	1st Slip Load (lbs)	Peak Load (lbs)	Slv HR/cut ΔID (1) (inch)	HR/HE ΔOD (inch)	HR/Cut ΔOD (inch)	TOF (2) to Cut (inch)
NA-1 (3)	1440	4800	0.008/0.009	0.022	0.008	1.12
NA-2 (3)	1640 (4)	6500	0.008/0.009	0.021	0.008	1.12
NA-3	1760	6560	0.006/0.007	0.018	0.006	1.12
NA-4	2340	6000	0.008/0.009	0.011	0.008	1.19
NAFT-2	2000	7500 (5)	0.005/0.006	0.015	0.005	1.12
NAFT-3	1925	5700 (6)	0.006/0.007	0.016	0.006	1.06

Notes:

(1) The sleeve HR ID value is estimated based on comparison of process verification samples and pre-assembly tube and sleeve wall thickness measurements.

(2) The term TOF refers to "top of hardroll flat" and is a measurement of the distance from the top or beginning of the hardroll flat length to the cut location.

(3) Premature torque out.

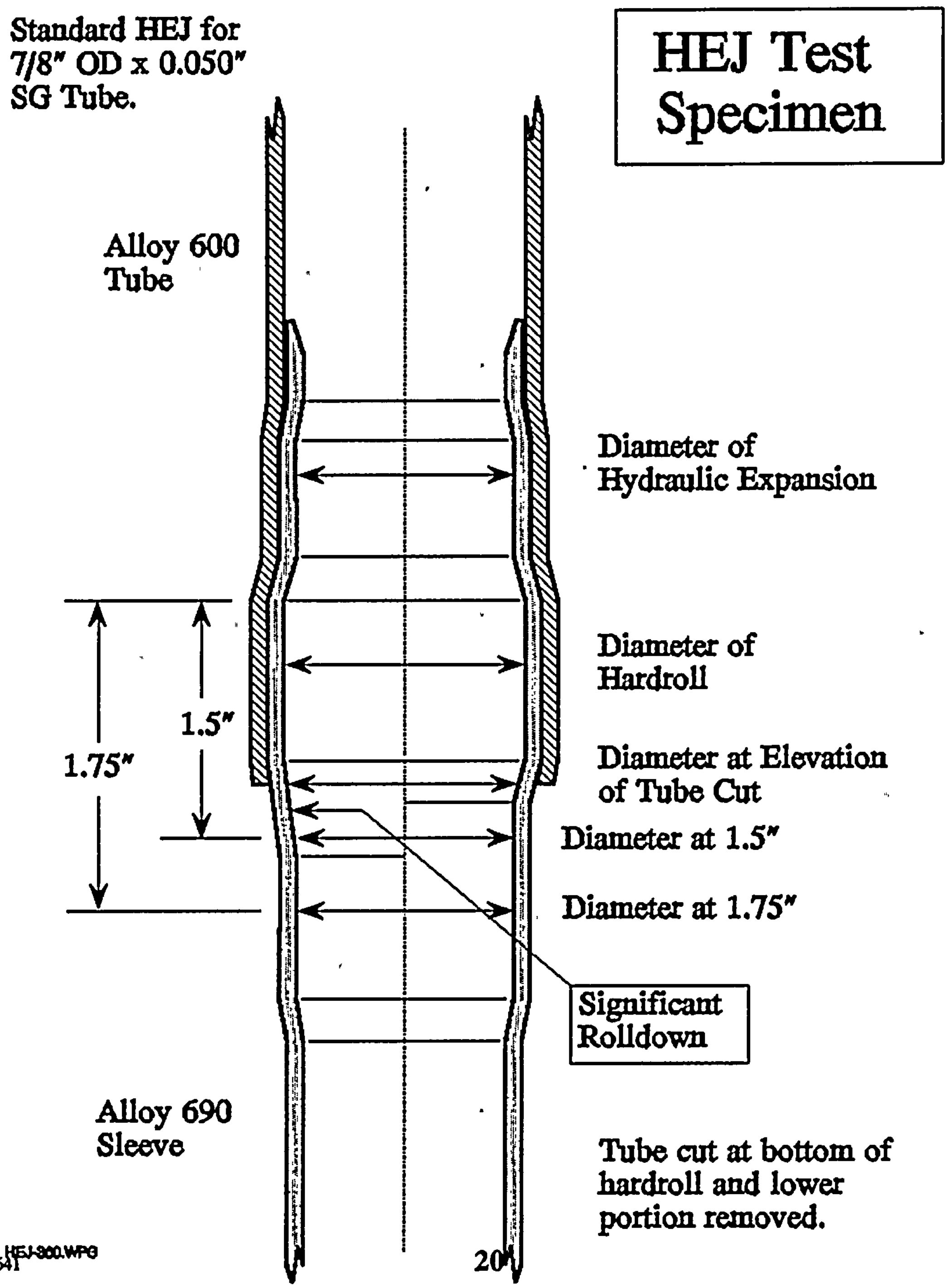
(4) First slip condition was hardly distinguishable. Samples did not experience a slippage as all others but more of a slight decrease in the load buildup rate.

(5) Tube non-degraded ligament broke at a load of 7500 lbs. Test manually stopped at this point.

(6) Tube non-degraded ligament broke at a load of 1925 lbs. Sleeve started to slip in tube at bottom hardroll at about 3250 lbs. Test manually halted at this point.

FIGURE 4-1

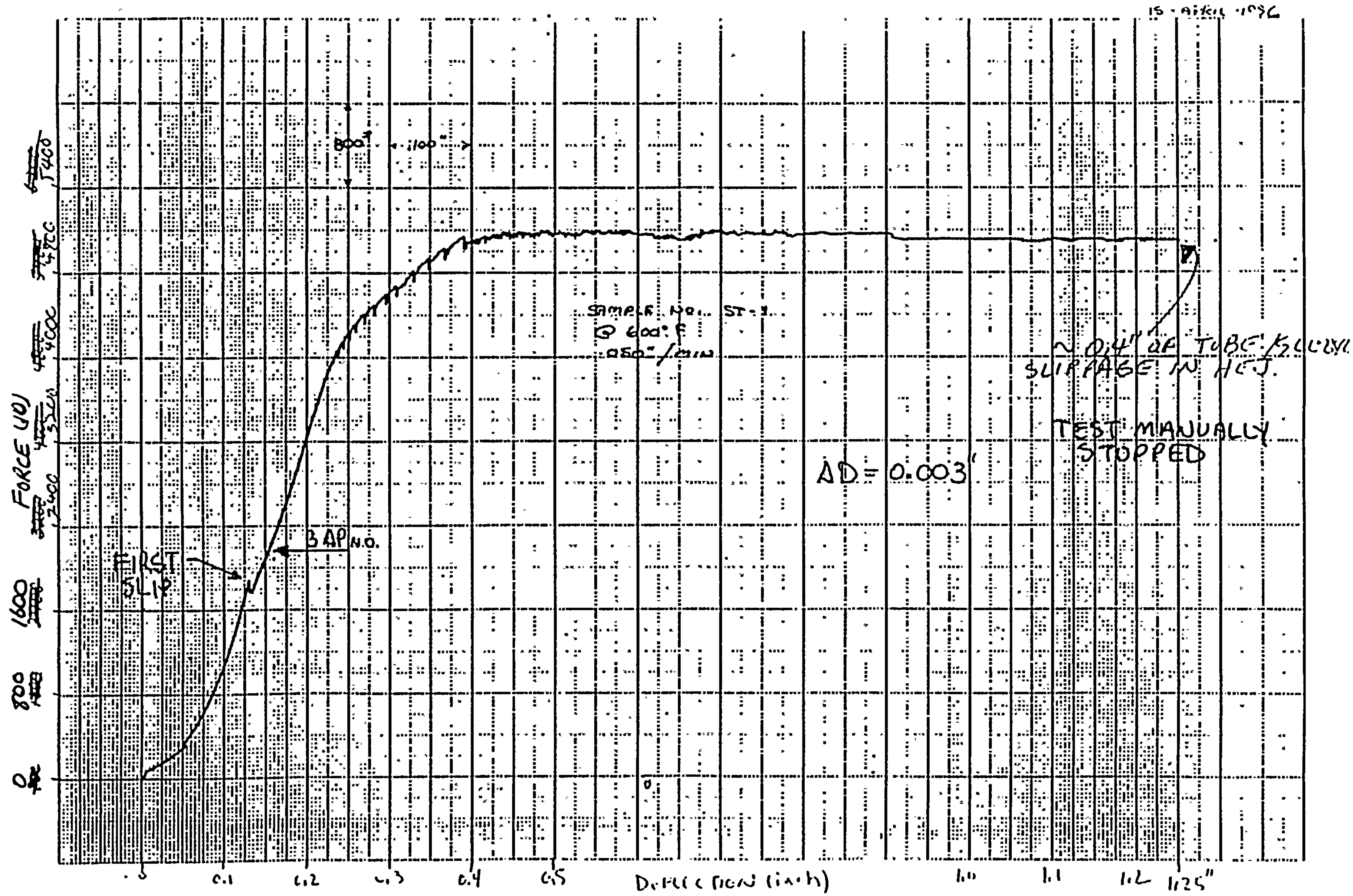
DIAMETER PROFILES FOR NON-CONDITIONED SAMPLES #5 AND #17 from 1996 TESTING PROGRAM



FIG[illegible]4-2a

TYPICAL RESISTIVE LOAD vs. TOTAL MACHINE DISPLACEMENT CURVE FOR CONDITIONED SPECIMENS:
1996 TEST DATA

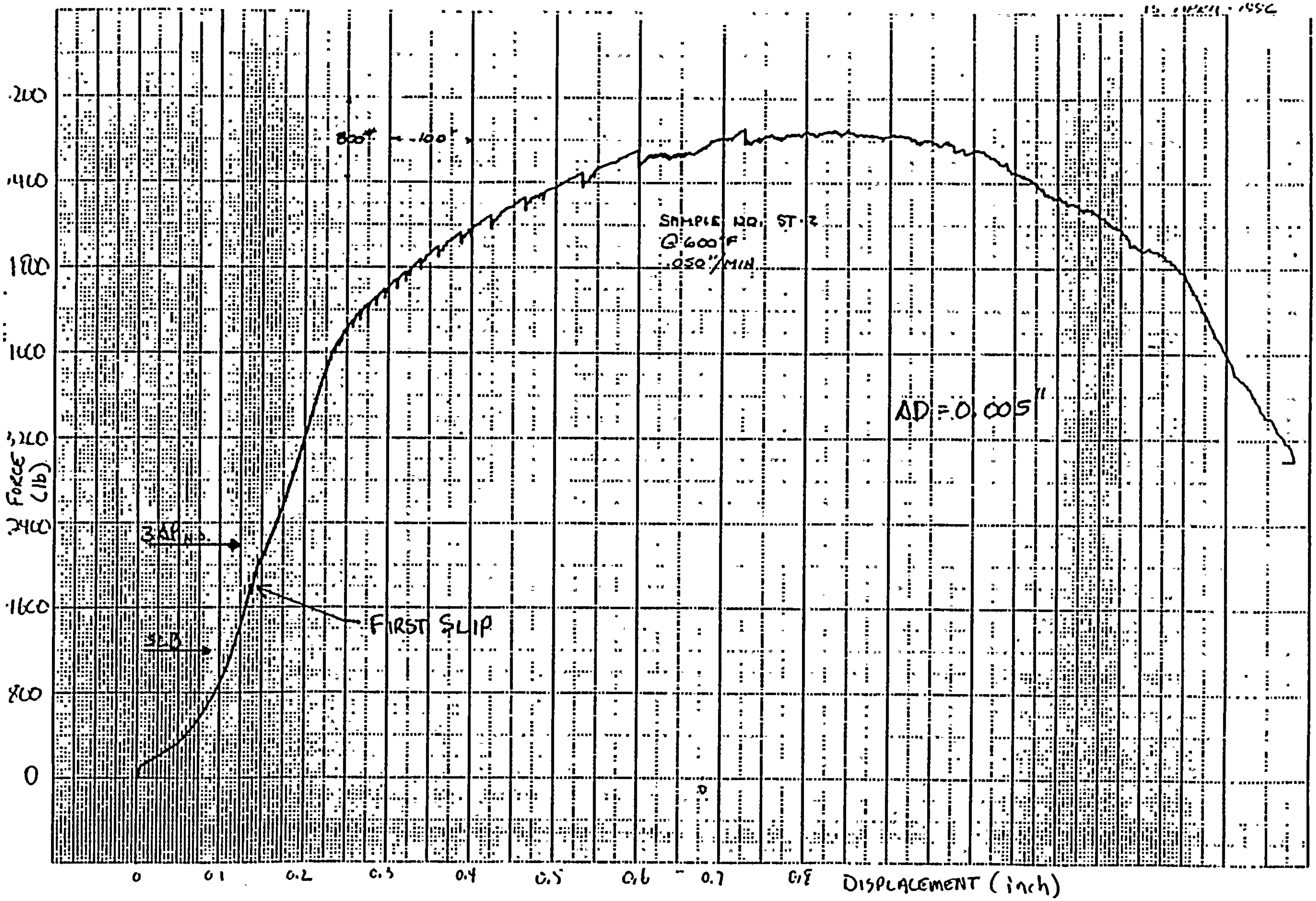
(Note: Total machine displacement includes slippage errors due to setting of OD gripper jaws, specimen elongation, both elastic and plastic, and test rig elasticity)



FIG[illegible]4-2b

TYPICAL RESISTIVE LOAD vs. TOTAL MACHINE DISPLACEMENT CURVE FOR CONDITIONED SPECIMENS: 1996 TEST DATA

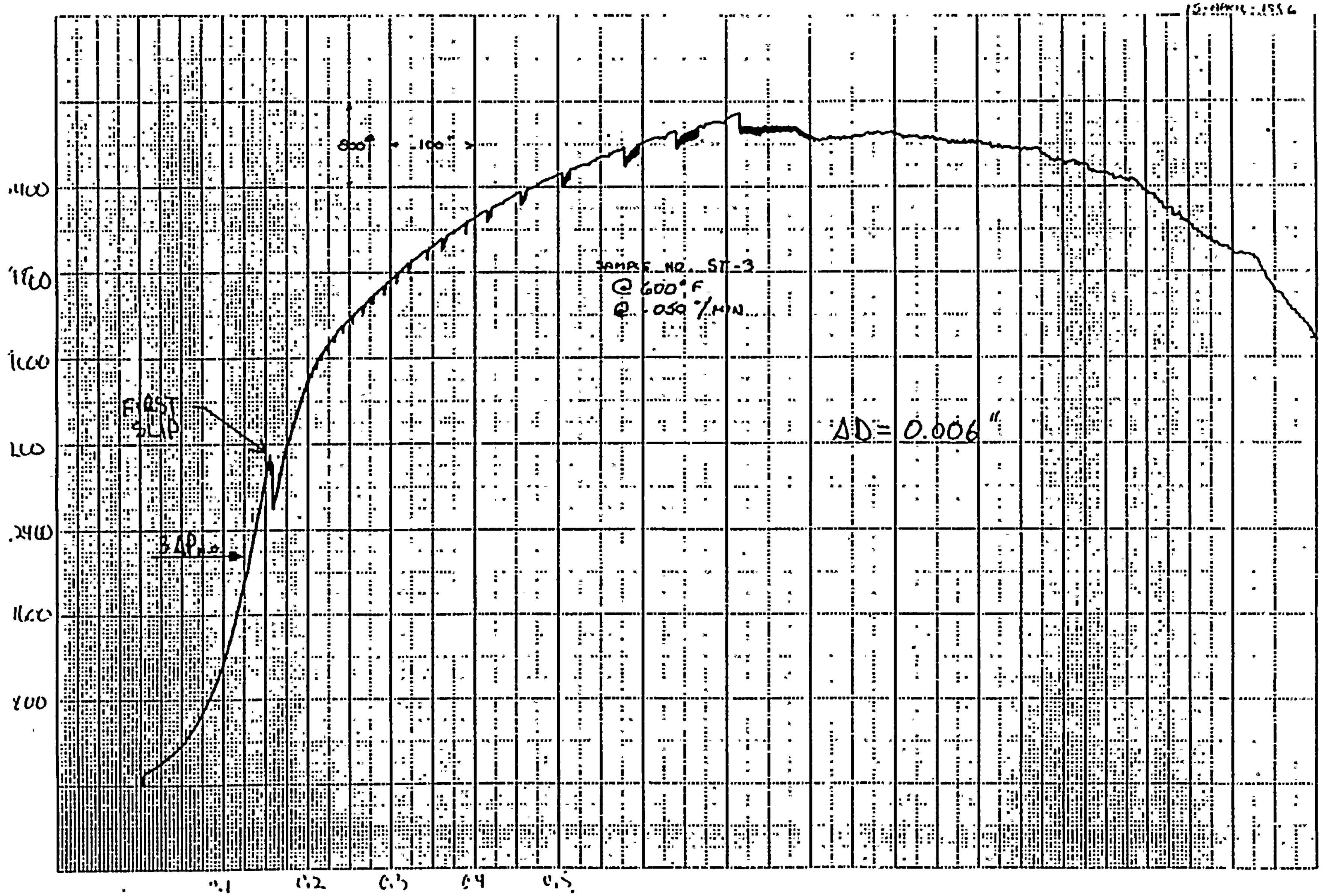
(Note: Total machine displacement includes slippage errors due to setting of OD gripper jaws, specimen elongation, both elastic and plastic, and test rig elasticity)



FIGU -2c

TYPICAL RESISTIVE LOAD vs. TOTAL MACHINE DISPLACEMENT CURVE FOR CONDITIONED SPECIMENS: 1996 TEST DATA

(Note: Total machine displacement includes slippage errors due to setting of OD gripper jaws, specimen elongation, both elastic and plastic, and test rig elasticity)



FIGU -2d

TYPICAL RESISTIVE LOAD vs. TOTAL MACHINE DISPLACEMENT CURVE FOR CONDITIONED SPECIMENS: 1996 TEST DATA

(Note: Total machine displacement includes slippage errors due to setting of OD gripper jaws, specimen elongation, both elastic and plastic, and test rig elasticity)

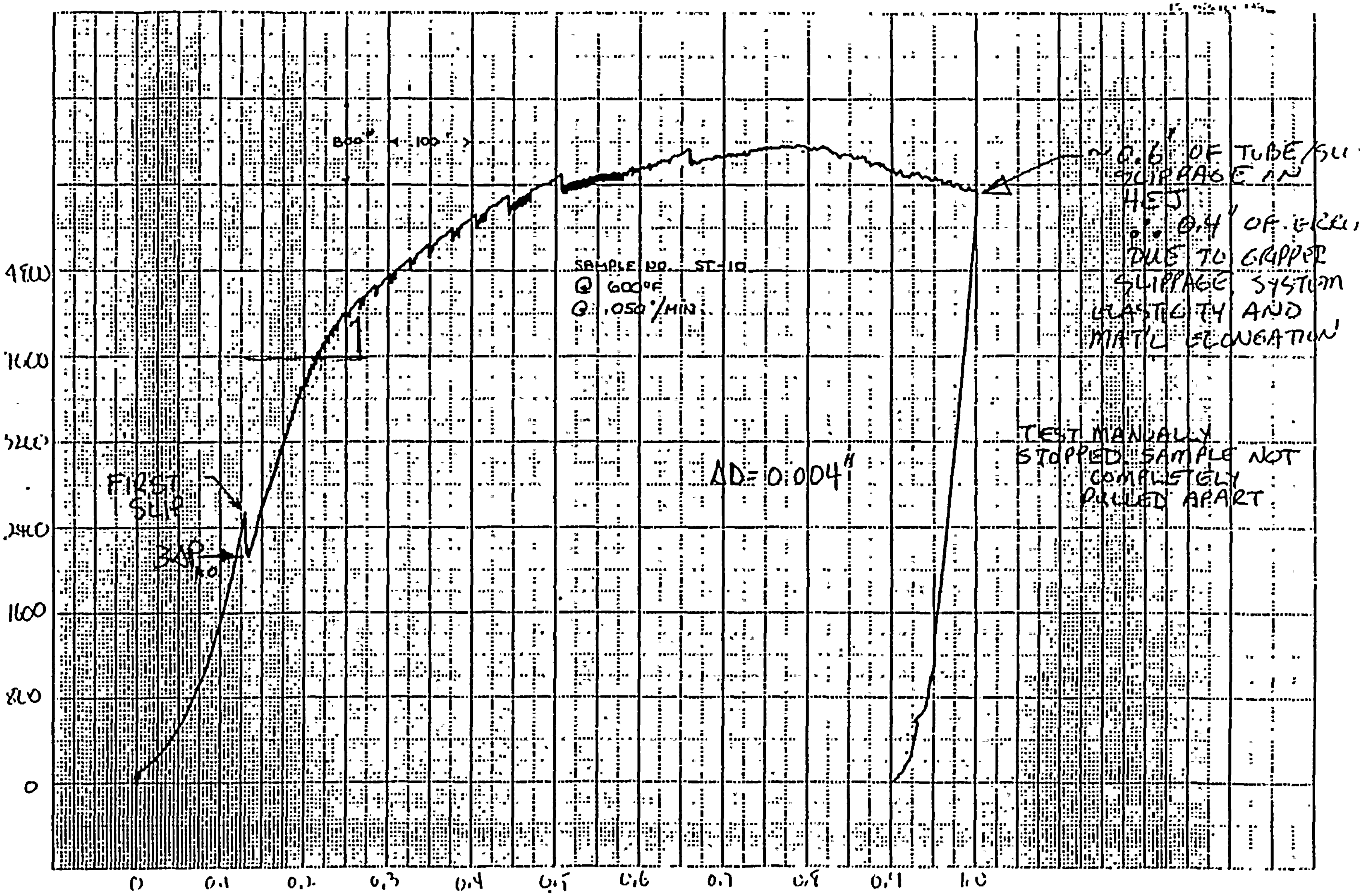


FIGURE 4-3

PEAK LOAD vs. DIAMETRAL INTERFERENCE: CONDITIONED SPECIMENS

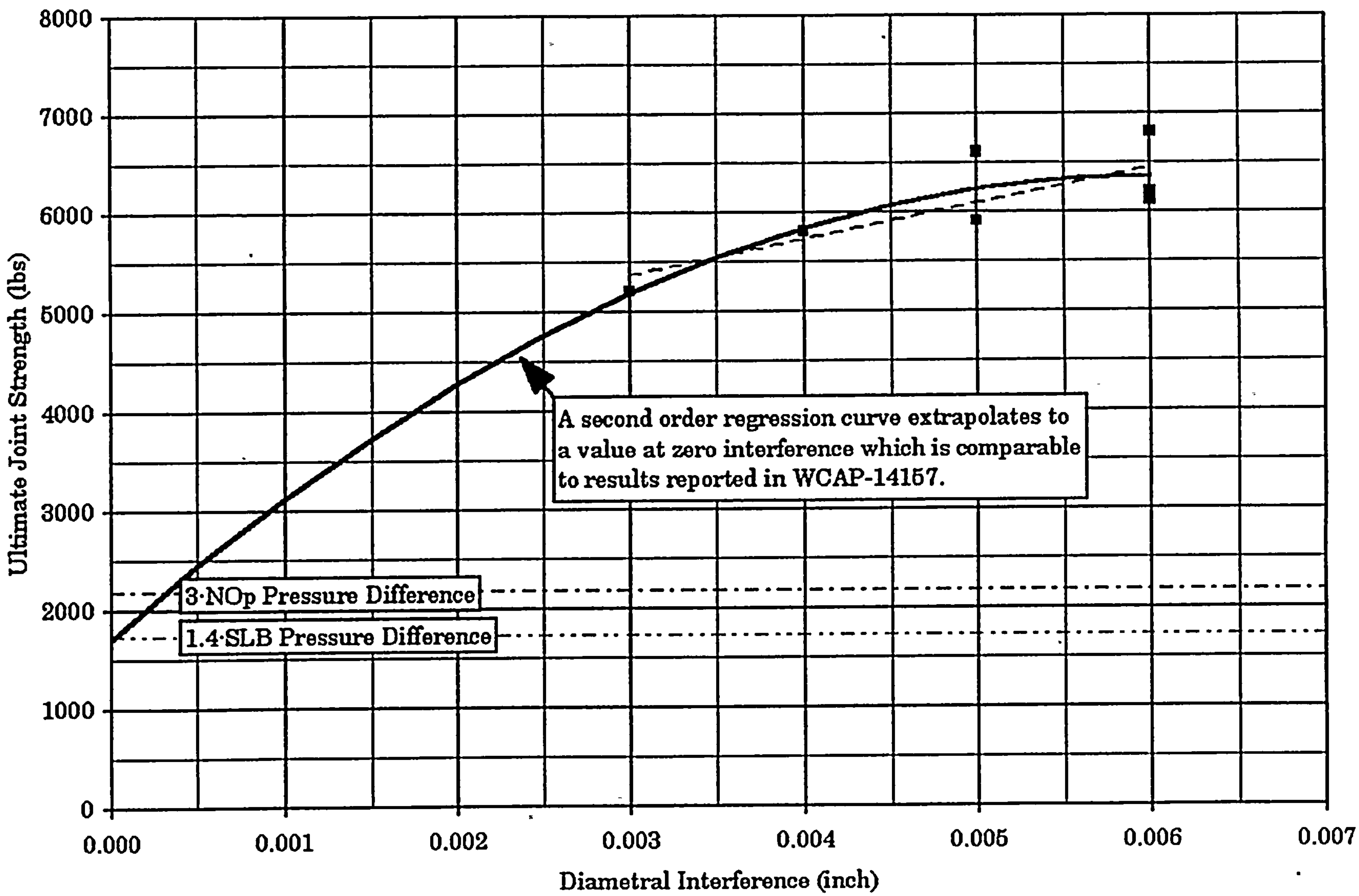


FIGURE 4-4

FIRST SLIP vs. DIAMETRAL INTERFERENCE: CONDITIONED SPECIMENS

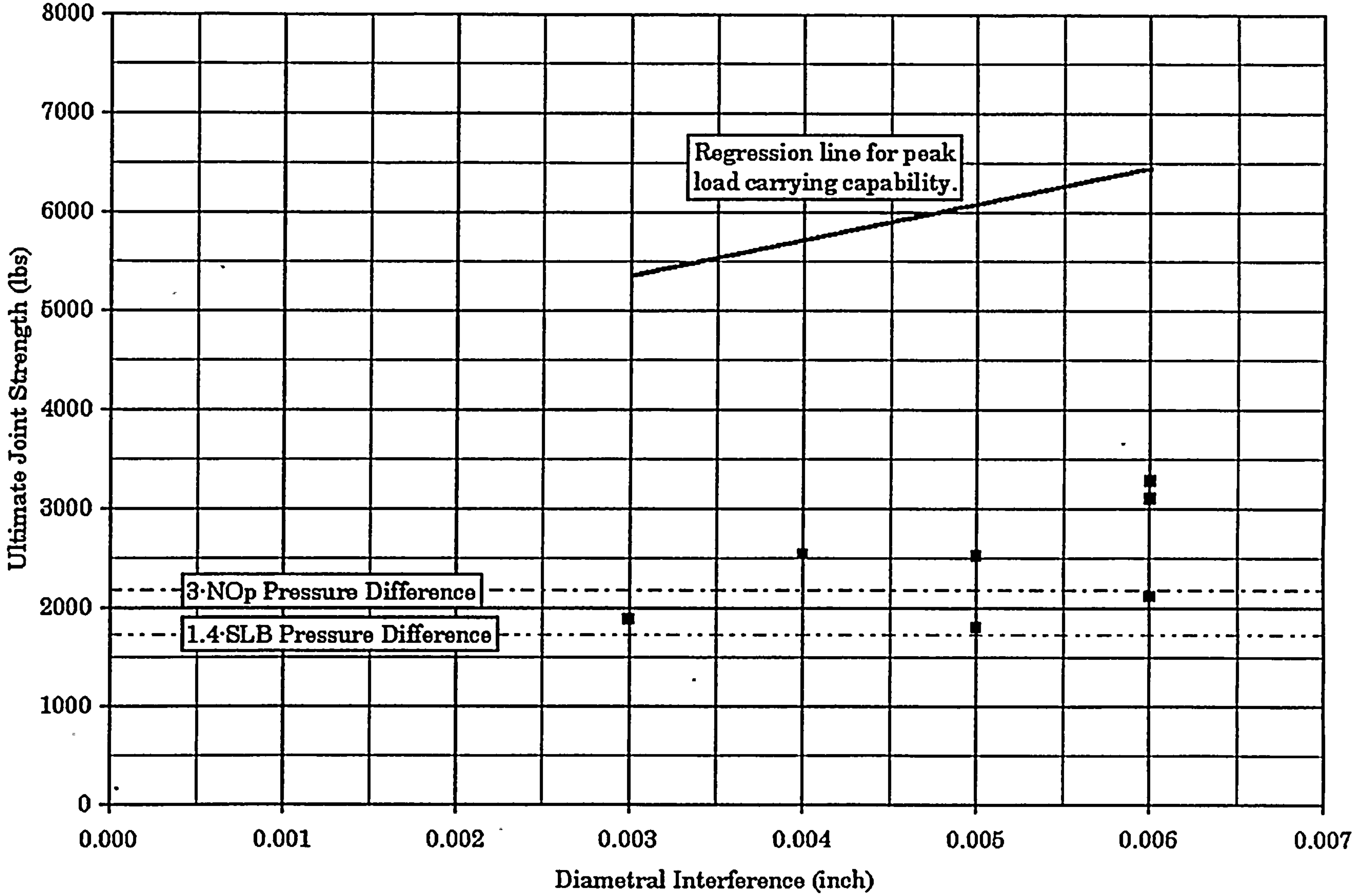
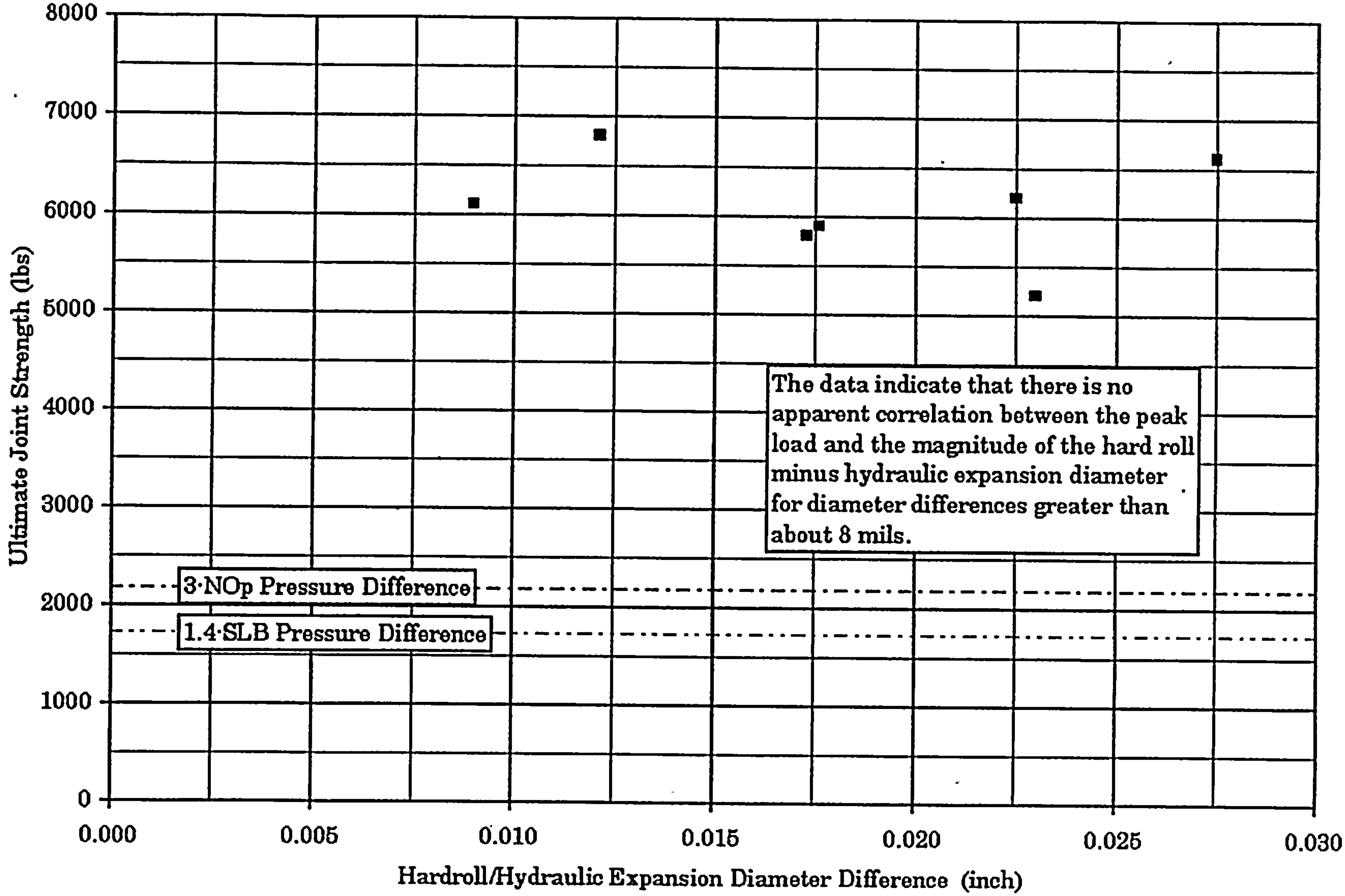


FIGURE 4-5
PEAK LOAD vs. HARDROLL/HYDRUALIC REGION ΔD



5.0 CONCLUSIONS

5.1 Structural Considerations

Based on the above discussions, it is concluded that an actual diameter difference of 0.003 inch between the sleeve ID hardroll peak diameter immediately above the hardroll lower transition and the sleeve ID adjacent to the PTI will provide for peak load bearing capability far in excess of the most limiting RG 1.121 loading. The projected first slip values for such tubes are consistent with the tube end cap load developed by the theoretical loading of 1.4 times the SLB end cap load and therefore exceed the actual pressure developed end cap loading at SLB conditions.

The developed inside diameter ΔD value of 0.003 inch (actual) applies equally to the range of upper hardroll expansions experienced at Kewaunee. In 1988, the average of the sleeve ID hardroll to sleeve ID hydraulically expanded difference was approximately 0.034 inch while the average for the 1989 and 1991 sleeve installations was 0.028 inch and 0.024 inch, respectively. The distribution of indications suggests that the incidence of parent tube degradation is not attributed solely to size of the roll expansion. The eddy current data for the 1995 inspection indicates that most crack locations are greater than 1.3 inch below the top of the hardroll flat length, and considering that the robotic tooling provided rolldown restraint, the following expectations can be established;

1) Crack locations for the majority of the previously plugged sleeved tubes (based on recorded crack elevations below the bottom of the hardroll upper transition) provides for hardroll peak to crack ΔD's of greater than 0.003 inch, and most likely on the order of 0.007 inch. At such ΔDs, the total load capability of the HEJ would be expected to approach the ultimate strength capability of the sleeve itself.

2) The field tubes will contain less rolldown than the test samples. This more pronounced interference lip should provide for increased structural integrity characteristics over the test data base. The honing process performed on the field tubes has a beneficial impact upon both the first slip and peak load capabilities.

Additional considerations and observations regarding the Kewaunee steam generators can be presented which provide a defense-in-depth approach.

1) The revised repair boundary criterion does not take credit for the frictional force between the tube and the TSP. Experience with pulling tubes at Kewaunee and other plants indicates that these forces can be significant. Industry data indicates that for corrosion product packed TSP intersections that the minimum pull forces are approximately 80 lbs per intersection. If denting of the tube is experienced, the pull forces can exceed several

thousand pounds per intersection. In 1990, two tubes were removed from S/G B at Kewaunee. The is first tube was cut below the fourth TSP. After tube roll breakaway, the load increased to 18,000 lbs, at which point the tube broke. A second tube was cut below the first TSP. After tube roll breakaway, peak load increased to 14,000 lbs. Tube roll breakaway forces were consistent for both tubes. Once the roll is removed, the remaining forces are generated totally by the corrosion product packing in the tubesheet crevice and TSP intersections. If the tubesheet corrosion product packing conditions were similar for both tubes then 4,000 lbs load capacity was developed among the first three TSP intersections for the first tube. This resistance load nearly doubles the 3ΔP end cap loading. Tube pull results from other plants with no detectable denting at the TSP intersections indicate tube pull forces of approximately 925 lbs per intersection to 2650 lbs per intersection.

2) The crack simulants used were very conservative. The removed tube data from Kewaunee showed that the degradation consisted of aplanar multi-ligament indications instead of a single throughwall flaw. The tests which utilized tubes with less than 100% throughwall slits showed that the first slip values were slightly elevated but that the peak load capability was well in excess of the throughwall slit tubes. The segmented morphology of the pulled tubes would effectively increase the interference lip and act to cause the peak resistive loads to approach those for the cases where tubes were machined away below the hardroll lower transition, resulting in resistive loads approximately equal to the ultimate strength of the sleeve itself.

3) The previously presented axial displacement analysis contained in WCAP-14446 showed that tube axial displacement of approximately 1.1" is required before substantial leakage can occur, and based solely on an available flow area basis, tube axial displacement of approximately 3 inches is required before tube rupture release rates are realized. The structural integrity testing concluded that slippage at actual worst case pressure end cap loads will not occur. Finally, if it is postulated that a tube were to experience some level of slippage, the load capacity balancing the pressure end cap load will be developed immediately upon the onset of galling between the tube and sleeve in the hardroll expanded region. The limits of tube axial displacement, neglecting the effects of TSP crevice packing, for *any* tube, is approximately 1.1" at SLB conditions, as described in WCAP-14446.

5.2 Leakage Considerations

Since the pressure developed end cap loading is less than the provided first slip resistance, there is no basis to exclude the previously recorded leak test data for tubes with 240° throughwall slits

located at a diameter difference of 0.000 to 0.001 inch. Leak test data for a sample separated at the top of the hardroll lower transition by tensile overload indicates SLB leakage of less than 0.001 gpm. The average SLB leakage of 0.0063 gpm is dominated by a few samples with leakage on the order of 0.012 to 0.016 gpm. Six of the nine specimens used for the leak testing had leakage less than 0.01 gpm and 4 of these 6 had leakage of less than 0.00005 gpm. Therefore, a SLB leakage allowance of 0.025 gpm per tube left in service as a result of application of the ΔD criteria represents a conservative bounding value in light of the limited data base. SLB leakage is to be neglected for indications with ΔD values of greater than 0.013 inch. The leak test results revisited here indicate that SLB leakage of 0.0025 gpm per indication would be expected for a ΔD of about 0.019 inch. The conservative allowance of 0.025 gpm per tube with ΔD values of 0.004 to 0.013 inch are expected to conservatively account for any leakage from tubes with ΔD's greater than 0.013 inch. The basis for selection of a lower bound for leakage allowance of 0.013 ΔD is that the minimum acceptable sleeve hardroll ID minus sleeve hydraulically expanded ID is 0.013 inch. For small roll expansions this will ensure that the elevation of the PTI is below the hardroll lower transition while for nominal and large PTIs the interference lip will be sufficient to preclude a large leakage event.

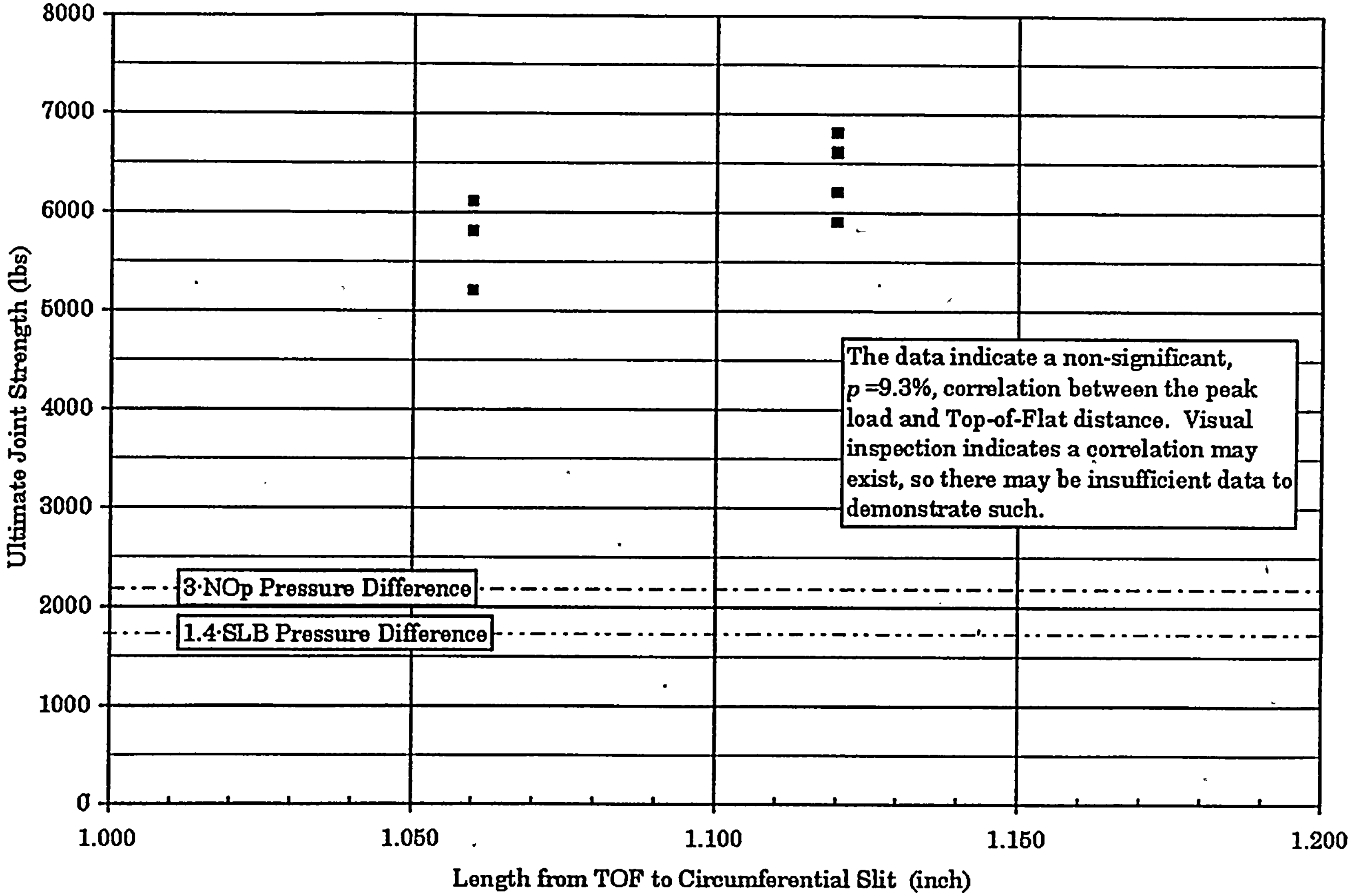
5.3 Validation of Previous HEJ Alternate Repair Criteria

The two previous HEJ license amendment proposals which were either denied or not decided upon yet can be validated by the latest set of tests. The structural test specimens had crack elevations of 1.06 (3 specimens) and 1.12 inch (4 specimens) below the top of the hardroll upper transition. All specimens exhibited peak load capacity far in excess of the most limiting RG 1.121 loading, and first slip values all exceeded the theoretical loading of 1.4 times the SLB pressure developed end cap loading. Therefore, the 1.1" F* analogous criteria proposed by American Electric Power in 1995 was verified by the current test results. A graphical representation of the structural integrity testing results for the conditioned tube samples using the measured distance below the top of the hardroll flat is provided in Figure 5-1. From Figure 5-1, the predicted peak load at the proposed criteria limit of 1.1" of hardroll below the top of hardroll flat is approximately 6000 lbs, and would provide for a margin of approximately 3800 lbs to the $3\Delta P$ maximum RG limit of 2178 lbs end cap load.

The limited angle criteria proposed by the Wisconsin Electric Power Company had been structurally verified by the test program described in WCAP-14157, Addendum 1, however, eddy current uncertainties involved with crack angle measurement were not resolved. The currently proposed criteria and the criteria proposed by American Electric Power do not rely upon crack angular measurement, and conservatively assume the tube to be completely circumferentially separated at the crack location.

FIGURE 5-1

PEAK LOAD vs. TOP OF HARDROLL FLAT TO CUT LOCATION



FRAMATOME
TECHNOLOGIES

Integrated Nuclear Services

JHT/96-46
July 15, 1996

U. S. Nuclear Regulatory Commission
ATTN: Document Control Desk
Washington, D.C. 20555

Subject: Supplementary Information to FTI's Response to NRC's Request for Additional Information on BAW-10168, Volume II, Revision 2, October 1992; RSG LOCA - BWNT Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants.

Reference: J. H. Taylor to Document Control Desk, "Response to NRC's Request for Additional Information on BAW-10168, Volume II, Revision 2, October 1992; RSG LOCA - BWNT Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants," JHT/94-171, October 28, 1994.

Gentleman:

The reference transmitted FTI's response to an NRC request for additional information on topical report BAW-10168, Revision 2. The attachment provides supplemental information to the referenced response. The material enclosed herein is considered non-proprietary to Framatome Technologies.

Very truly yours,

J. H. Taylor, Manager
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In summary, FTI will use a discharge coefficient of 1.0 for the entire two-phase leak flow regime. All other aspects of our break modeling will remain unchanged. This methodology complies with the intent and requirements of Appendix K for SBLOCA. For SBLOCA transients predicting clad temperatures above 1800 F using the Appendix K technique, FTI will also analyze such cases using its variable C_d method. 1800 F will be the established transition point. Analyzing such cases with both techniques assures that the PCT will be conservatively predicted.

Partial Loop Seal Clearing: In response to questions regarding partial loop seal clearing, several additional SBLOCA cases were run using the plant model shown in Figure 1. Break sizes were varied from 1.6 to 2.0-inch to study the transition from no loop seal clearing to the clearing of the broken loop. It was found that RELAP5/MOD2-B&W predicts this transition for breaks between 1.9 and 2.0-inches. The liquid levels in the broken loop pump suction piping for these two cases are shown in Figures 2 and 3. Figure 4 shows the core liquid levels for the two cases. From Figure 4 it can be observed that the minimum core liquid levels of about 9.0-ft occur at about 1600 seconds and increase thereafter. For the 2.0-inch break, the core liquid level is about 10.0-ft at the time of loop seal clearing. The loop seal spillunder elevation corresponds to 8.0-ft height from the bottom of the core.

The steam velocity in the upside pump suction piping for the 2-inch break is shown in Figure 5. Once the steam venting process initiates, the head imbalance in the loop seal accelerates the steam flow and can be expected to reach a terminal velocity sufficient to clear the loop seal. For the 2-inch beak the terminal steam velocity in the upside pump suction piping reaches about 10.0 ft/s at the time of loop seal clearing as shown in Figure 5. Tuomisto and Kajanto[1] show that the loop will clear completely for steam velocity greater than 6.2 ft/s (1.9 m/s) at 870 psia (60 bar). This is based on the flooding criterion for large diameter vertical pipes, Kutateladze Number Ku (See Equation 5 in Reference 1) equals 3.2. This flooding criterion is defined as a zero downward flow of falling film on the tube surfaces. They also show that, at pressures above about 145 psia (10 bar), vertical flooding is the limiting mechanism for loop seal clearing rather than the droplet entrainment from the stratified liquid in the horizontal section of the loop seal. For the 2.0-inch break case, the system pressure is about 1000 psia and therefore the loop will clear for steam velocities lower than 6.2 ft/s. The 1.9-inch break case in ROSA (see response to Question 14) demonstrates the loop seal clearing mechanism discussed above. For these break sizes, it is possible to accumulate some of the liquid in the loop seal once the initial acceleration of steam is complete as observed in the test. This liquid fall back is also observed in the RELAP5 simulation of the 1.95-inch break case which is discussed at the end of this section.

Figure 3 shows that the liquid level in the upside of the loop seal section starts to decrease after about 1700 seconds. The void fractions in Nodes 255, 260, and 265 are shown in Figures 6 through 8, respectively. From these figures it can be seen that the liquid level decrease in the loop seal upside section is caused by the increase in void fraction in the pump volume (Node 260). Steam venting from the loop seal occurs only after about 2200 seconds as shown in Figure 6. The pump discharge piping on the other hand is highly voided after about 750 seconds due to the steam flow from the upper head spray nozzles into the downcomer. At about 1600 seconds the break junction void fraction increases rapidly from zero to a highly voided state and the flow in the cold

leg starts to oscillate. Injection of the cold ECC water into the highly voided cold leg and the break node amplify these oscillations. This results in a flow of steam from the cold leg into the pump volume. Note that in the broken loop, up until loop seal clearing, the HPI water is injected in to the Node 276 (a vertical node), and the CCI water is injected into the cold leg. The equilibrium option is selected in Node 276, making Node 276 a major source of oscillations. Stratified flow is expected in the pump discharge piping and RELAP5 allows only small condensation when the flow is stratified. The voiding of the pump node prior to loop seal clearing is discussed further in the next section.

To further study the possibility of predicting partial loop seal clearing, a 1.95-inch break case was run. The broken loop also cleared for this case. However, some liquid remained in the upside section and in the pump node, possibly as a liquid film on the pipe walls that fell back after the high steam flow period ended. This water eventually accumulated in Node 255 as shown in Figure 9. All other nodes in the loop seal were almost completely voided. The liquid did not fall into node 250, which represents the lowermost portion of the U-bend. This is consistent with the discussion in Reference 1.

Pump Noding Sensitivity Study

The broken loop pump suction noding for the base model is shown in Figure 10. To reduce early loop seal clearing, Node 248, representing the lower portion of the downside piping, was set at a small node height, 1 foot. The bottom of Node 248 coincides with the spill under elevation of the loop seal. Node 250 represents the horizontal portion of the U-bend and the height of this node is the radius of the pipe. Node 260 represents the pump. The height of Node 260 is 5.81 ft which is the actual height of the pump up to the centerline of the discharge piping. In RELAP5, the pump volume also uses the high mixing flow regime, and, therefore, slug flow (Wilson drag) is not used in this node, even though it is a vertical node.

The early voiding of the pump node for the 2.0-inch break case, as discussed in the previous section, may have been caused by the height of Node 260. To study the sensitivity of pump node size, the base input model was modified by dividing the pump volume into three nodes (259-1, 259-2, and 260) as shown in Figure 11. Node 260 still represents the pump. In this case the 2.0-inch break case did not clear the loop seal. For a 2.1-inch break case, the loop seal cleared after about 3300 seconds. Collapsed liquid levels in the loop seal and core and the void fractions in the loop seal nodes, pump node, and the pump discharge node of the broken loop are shown in Figures 12 through 22. From these figures the following observations can be made. Steam venting through the loop seal starts after Node 245 is highly voided. This occurs at about 1400 seconds. The void fraction in Node 259-1, which is part of the actual pump, is close to the void fraction in Node 258. Node 260 is highly voided and the void fraction in node 259-2 is somewhere between the values for Nodes 259-1 and 260. The void distribution in the upside U-bend, including the pump volume, is improved over that in the base calculation. The venting of steam causes the liquid level in the upside of the U-bend to decrease, reducing the core level depression. Figures 12, 14, and 15 show liquid level oscillations on the order of 1.0 foot in the down side of the U-bend from about 1500 seconds until the time of loop seal clearing, about 3300 seconds. The oscillations are mainly caused by the condensation of steam on the cold ECC water

injected into the cold legs. Rothe, Wallis, and Thrall[2] discussed the pressure and flow oscillations due to the condensation of steam on ECC water in the cold legs. CE[3] and Westinghouse 1/14 scale tests[2] (See Table X in Reference 2) both show condensation induced pressure oscillations on the order of 10 to 20 psi. Therefore, the RELAP5 calculated 1.0 foot oscillations are reasonable.

Conclusion

From this study the following conclusions are made. The transition from no loop seal clearing to clearing of one loop occurs within a narrow range of break sizes. Condensation-induced oscillations causes steam venting through the loop seal before the liquid level in the downside section of the loop seal reaches the spillunder elevation. This substantially reduces the possibility of core uncovery at the time of loop seal clearing for these break sizes. The core never uncoverd for the break sizes studied here.

The revised pump noding will be used in SBLOCA EM. However, this model change does not impact previous EM studies and benchmarks.

References

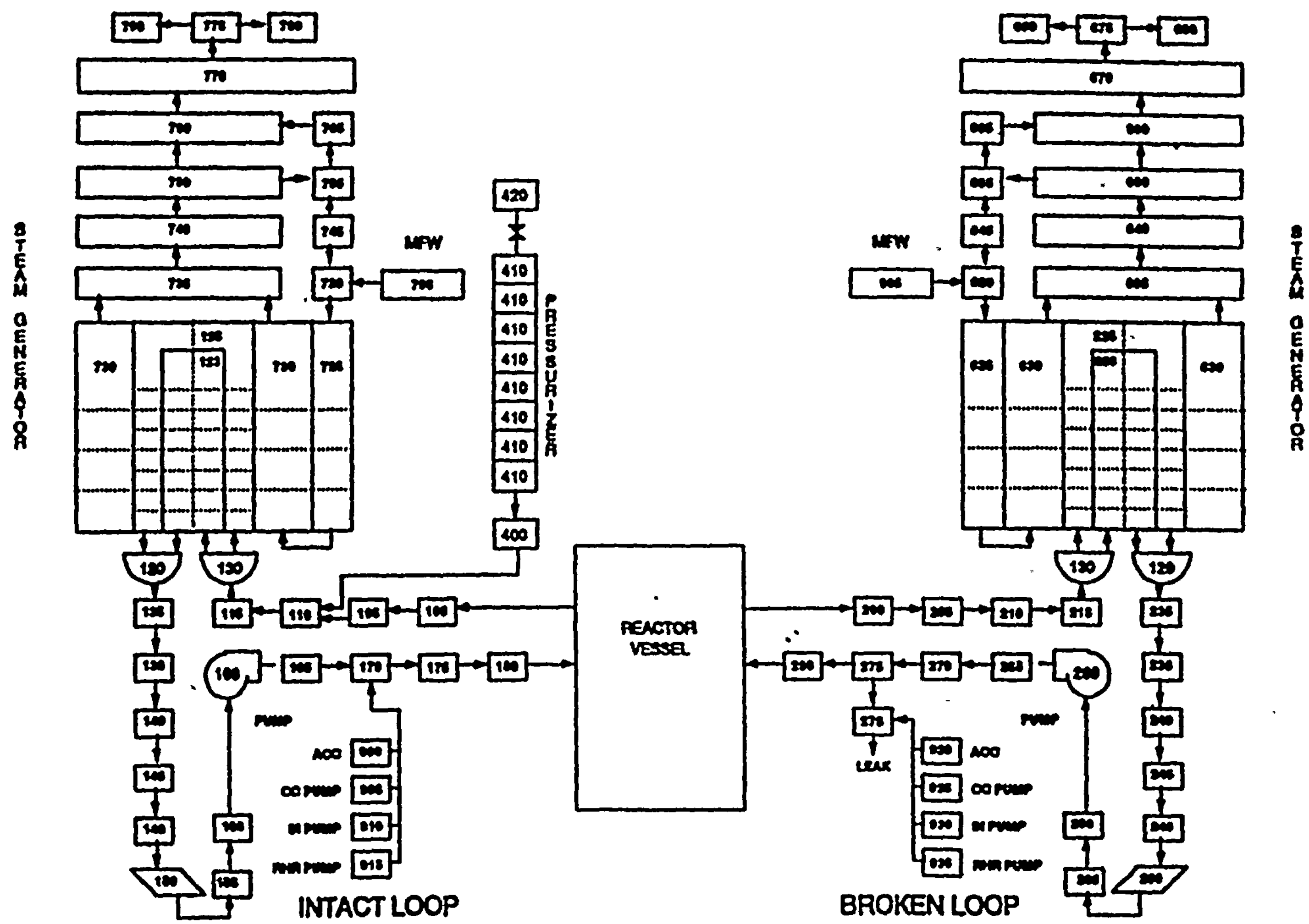
1. H. Tuomisto and P. Kajanto, "Two-Phase Flow in a Full Scale Facility," Nuc. Engg. And Design 107, pp 295-305, 1988.

2. P. H. Rothe, G. B. Wallis, and D. E. Thrall, Cold Leg ECC Flow Oscillations, EPRI NP-282, November 1976.

3. W. E. Burchill, P. A. Lowe, and J. R. Brodrik, Steam-Water Mixing Test program Task D: Formal Report for Task B and Final Report for the Steam Relief Phases of the Test Program, CENPD-101, AEC-C00-2244-1, October 1973.

Figure 5.9-2
RELAP5 Small Break LOCA Model
Sequoyah Noding for Primary Loops with Model 51 SGs

Fig-1
5



R5/2 1.9 INCH BREAK
RELAP5/MOD2 Ver 20.0HP

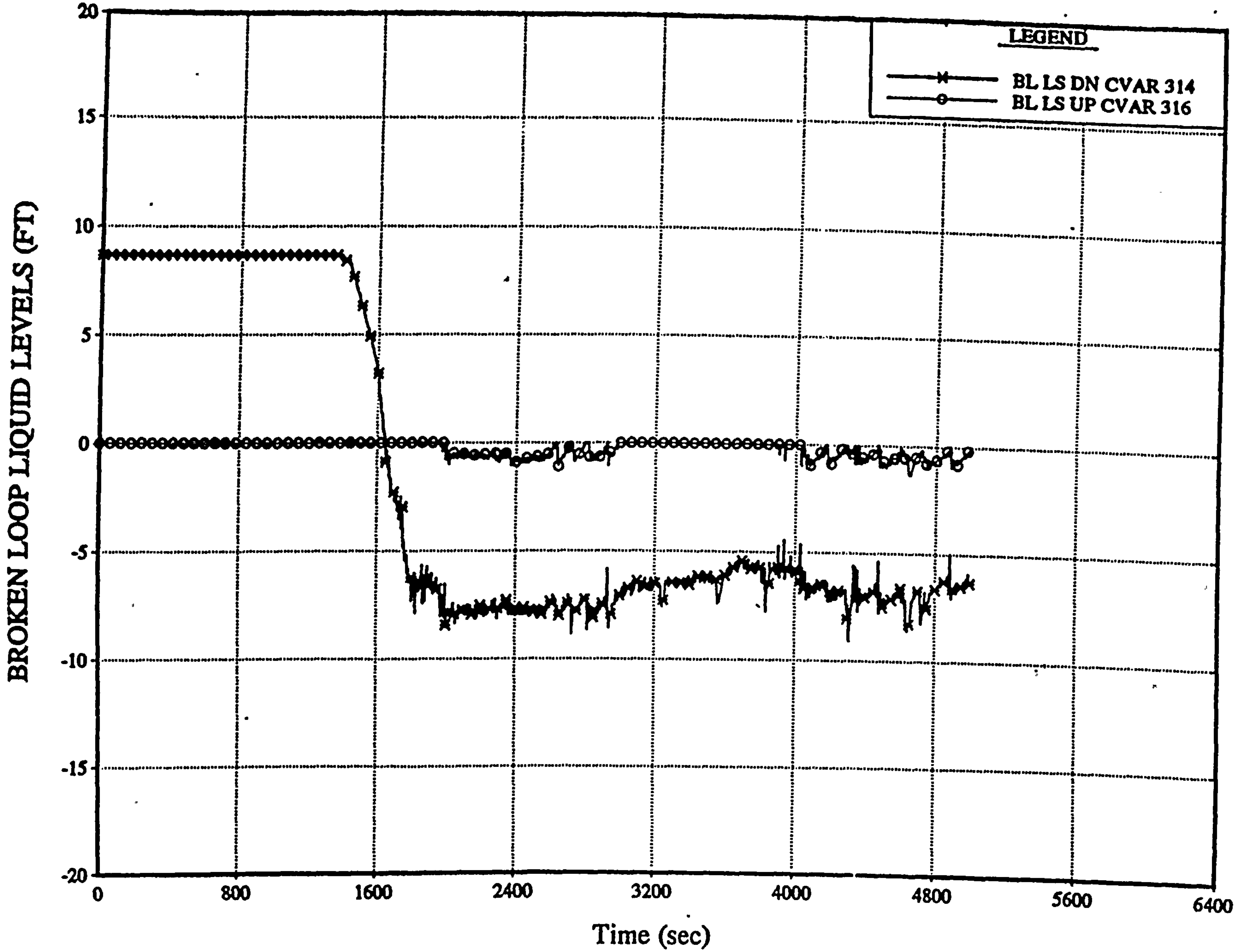


Fig 2

TOAW_CNTRL040

R5/2 2.0 & [illegible] INCH PD BREAKS
RELAP5/MOD2 Ver 20.0HP

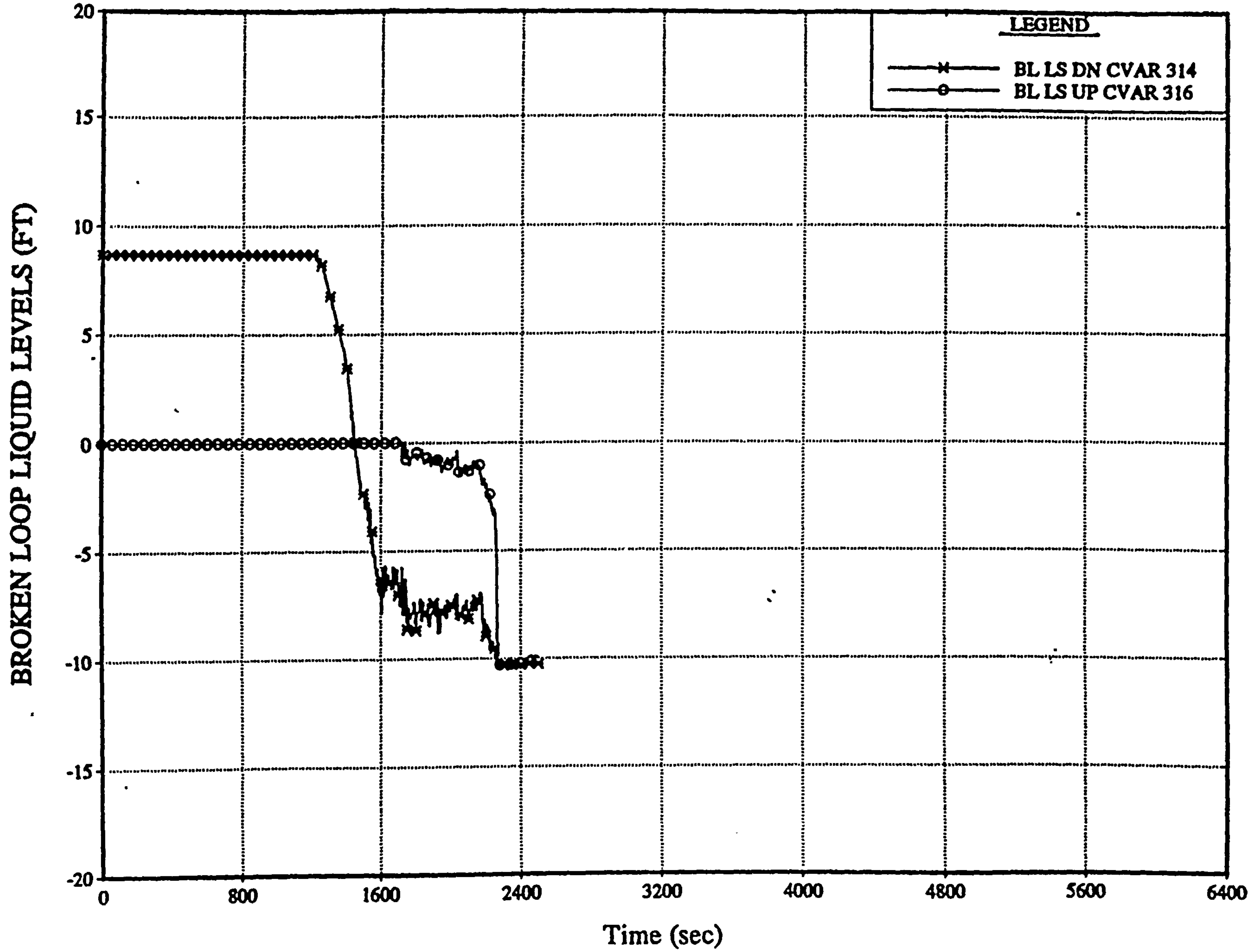


Fig 3

Fig-3

FAID CNTRL040

R5/2 2.0 & 1.9 [illegible] PD BREAKS

RELAP5/MOD2 Ver 20.0HP

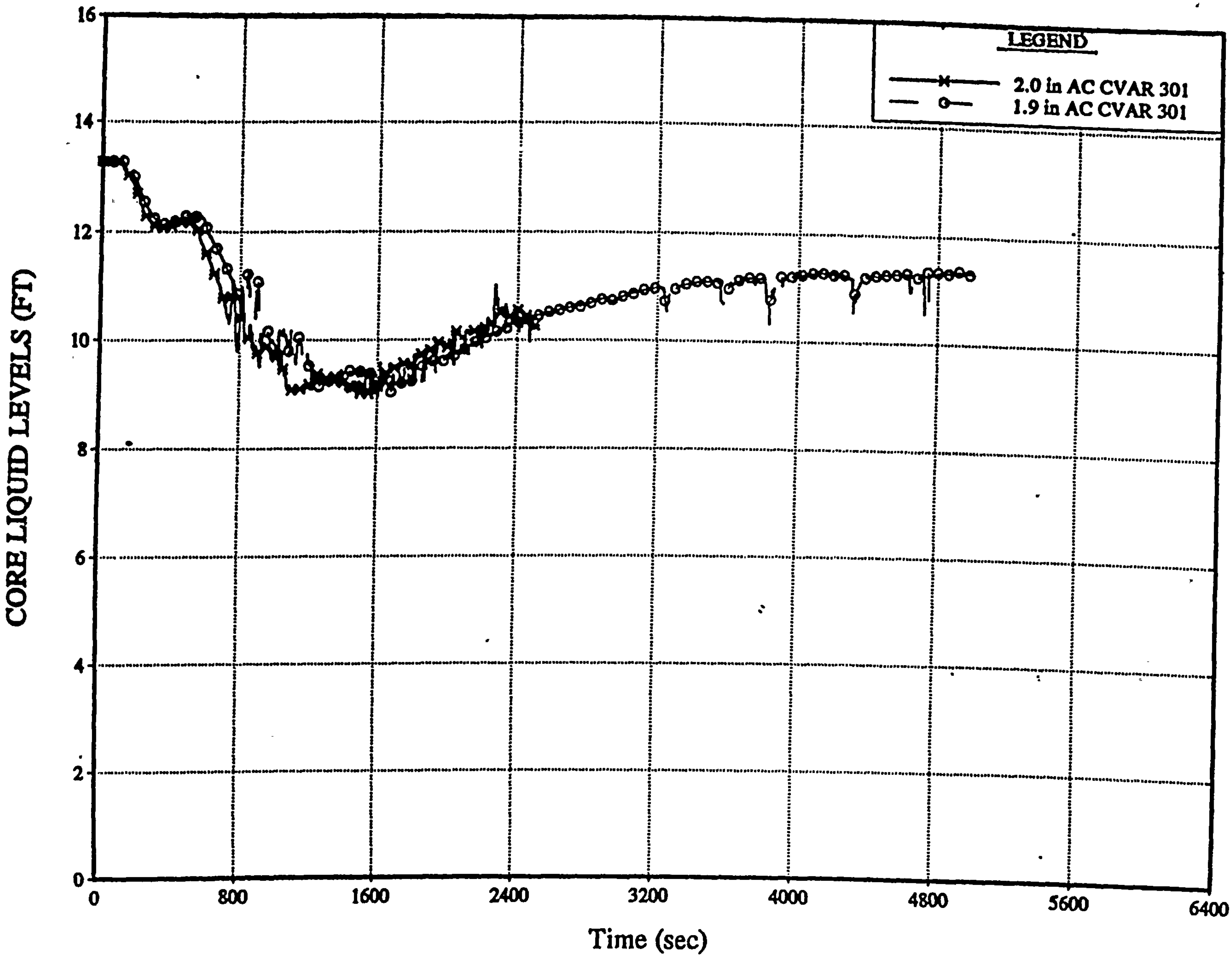
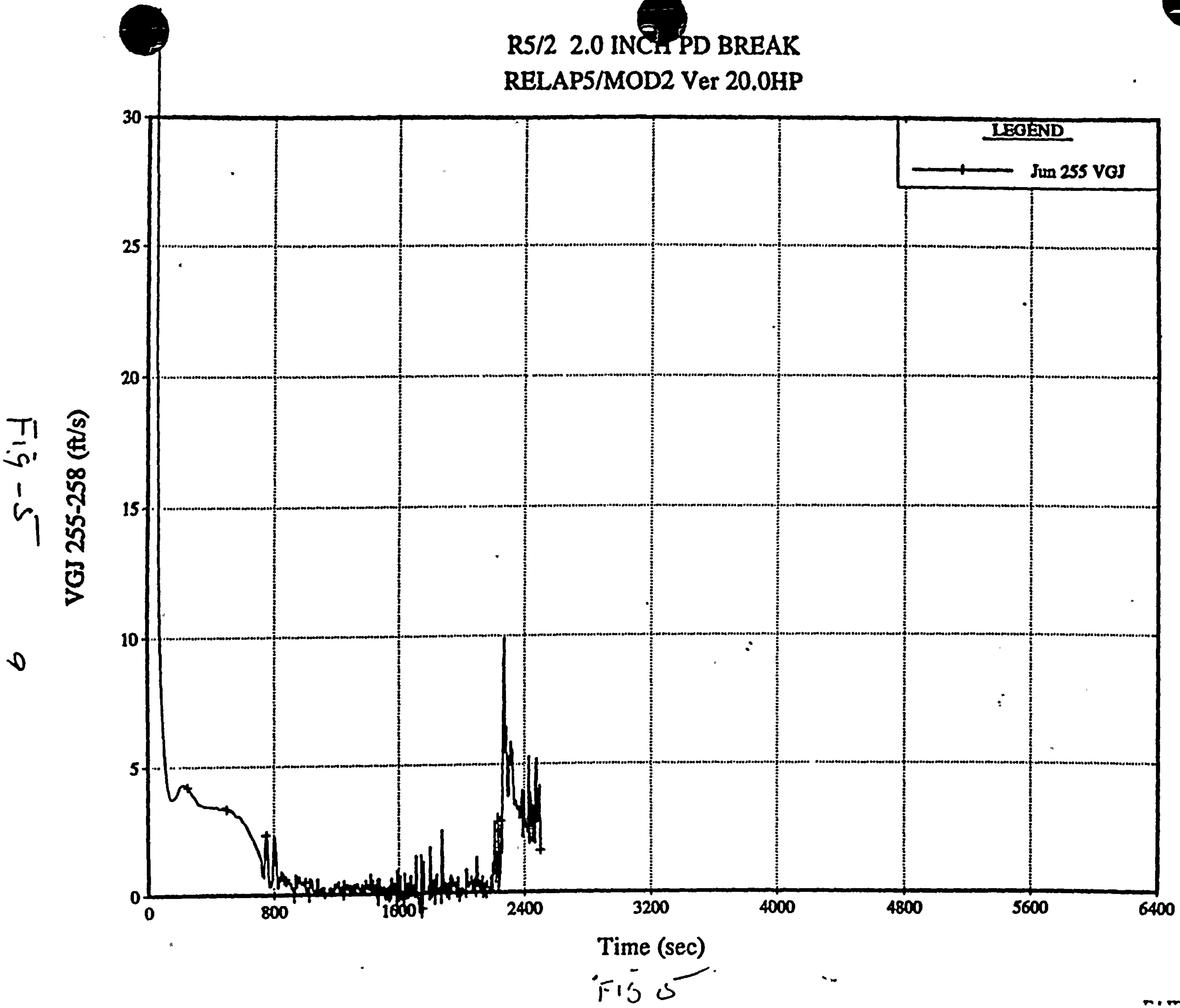


Fig 4

FAJD & TOAW


R5/2 2.0 INCH PD BREAK
RELAP5/MOD2 Ver 20.0HP
LEGEND
Jun 255 VGJ
VGJ 255-258 (ft/s)
Time (sec)
0
5
10
15
20
25
30
800
1600
2400
3200
4000
4800
5600
6400
Fig - 5
FIG 5

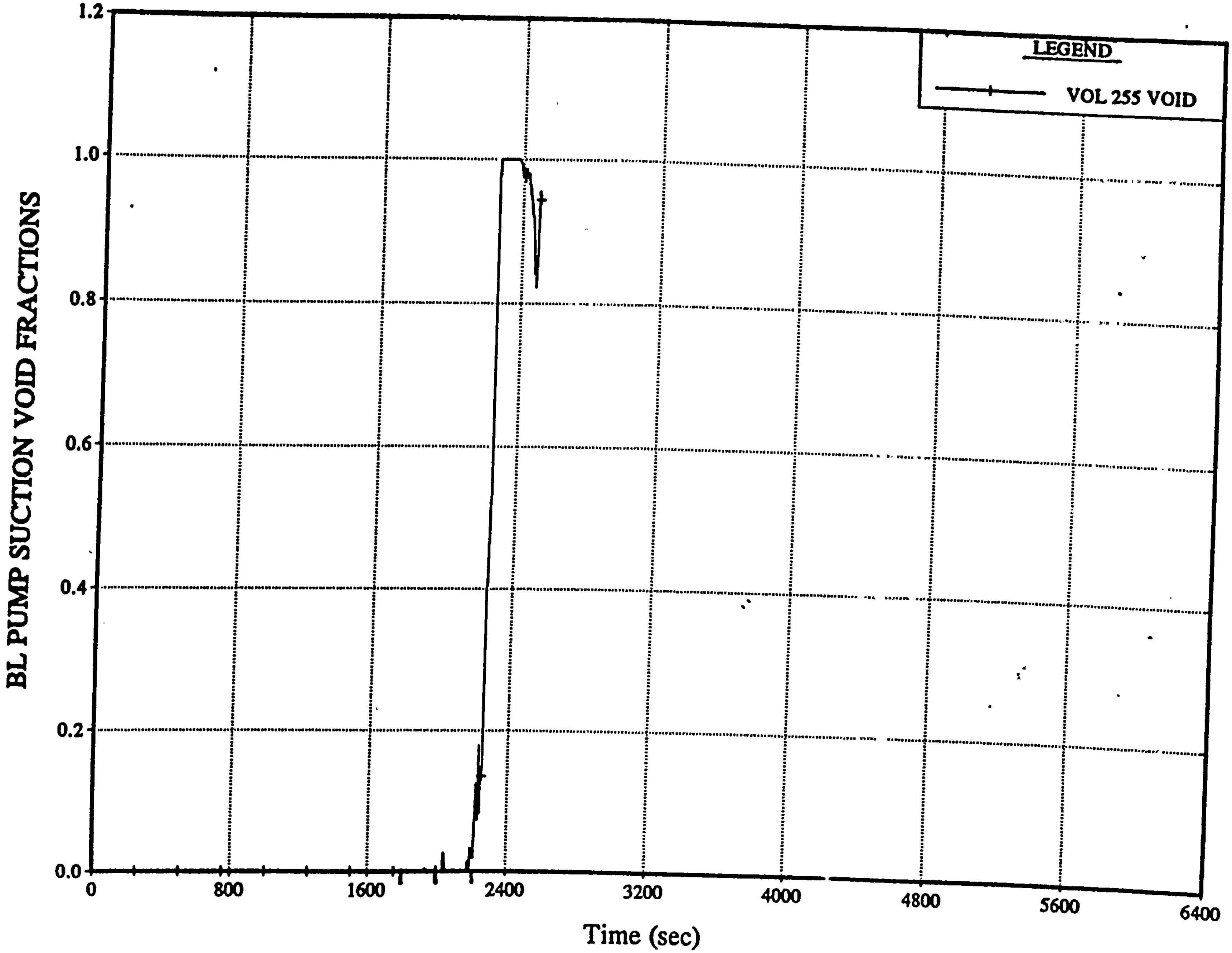

BL PUMP SUCTION VOID FRACTIONS
Time (sec)
LEGEND
VOL 255 VOID
0.0
0.2
0.4
0.6
0.8
1.0
1.2
0
800
1600
2400
3200
4000
4800
5600
6400

Fig 6

10 Fig - 6

SQN 2.0 in SBLOCA Case, sqnsb2b.in

Fig - 7

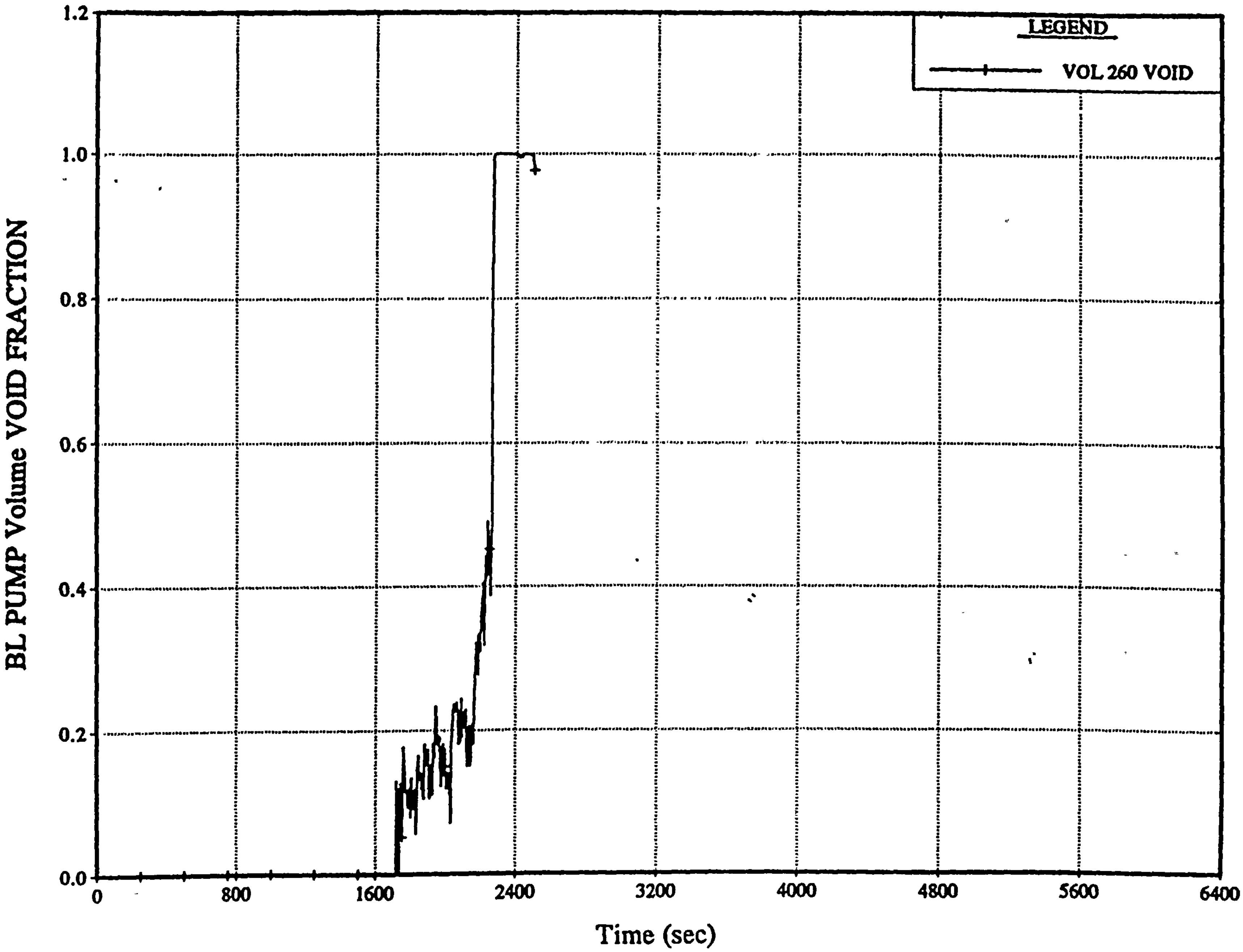


Fig 7

SQN 2.0 in SBL[illegible] Case, sqnsb2b.in

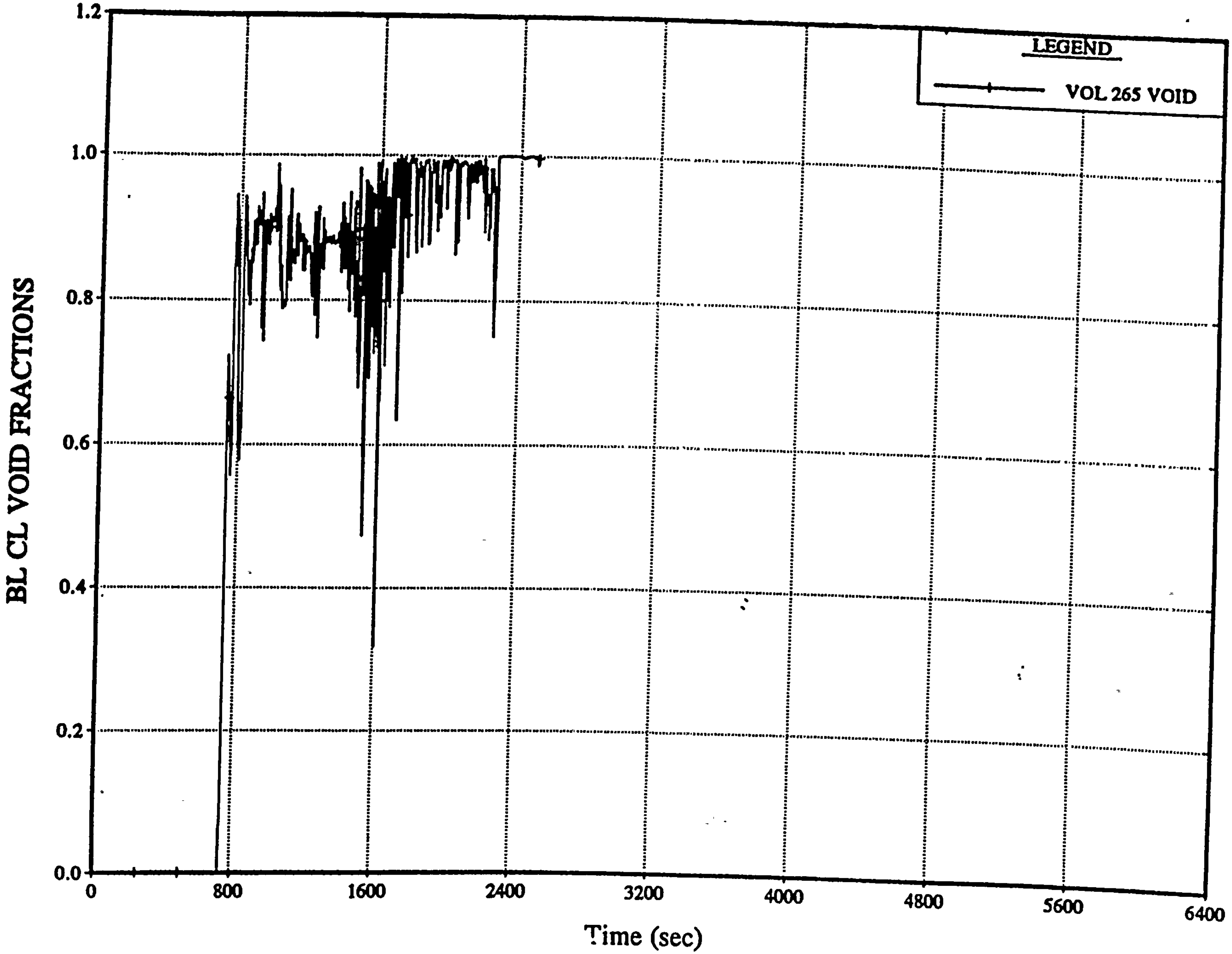
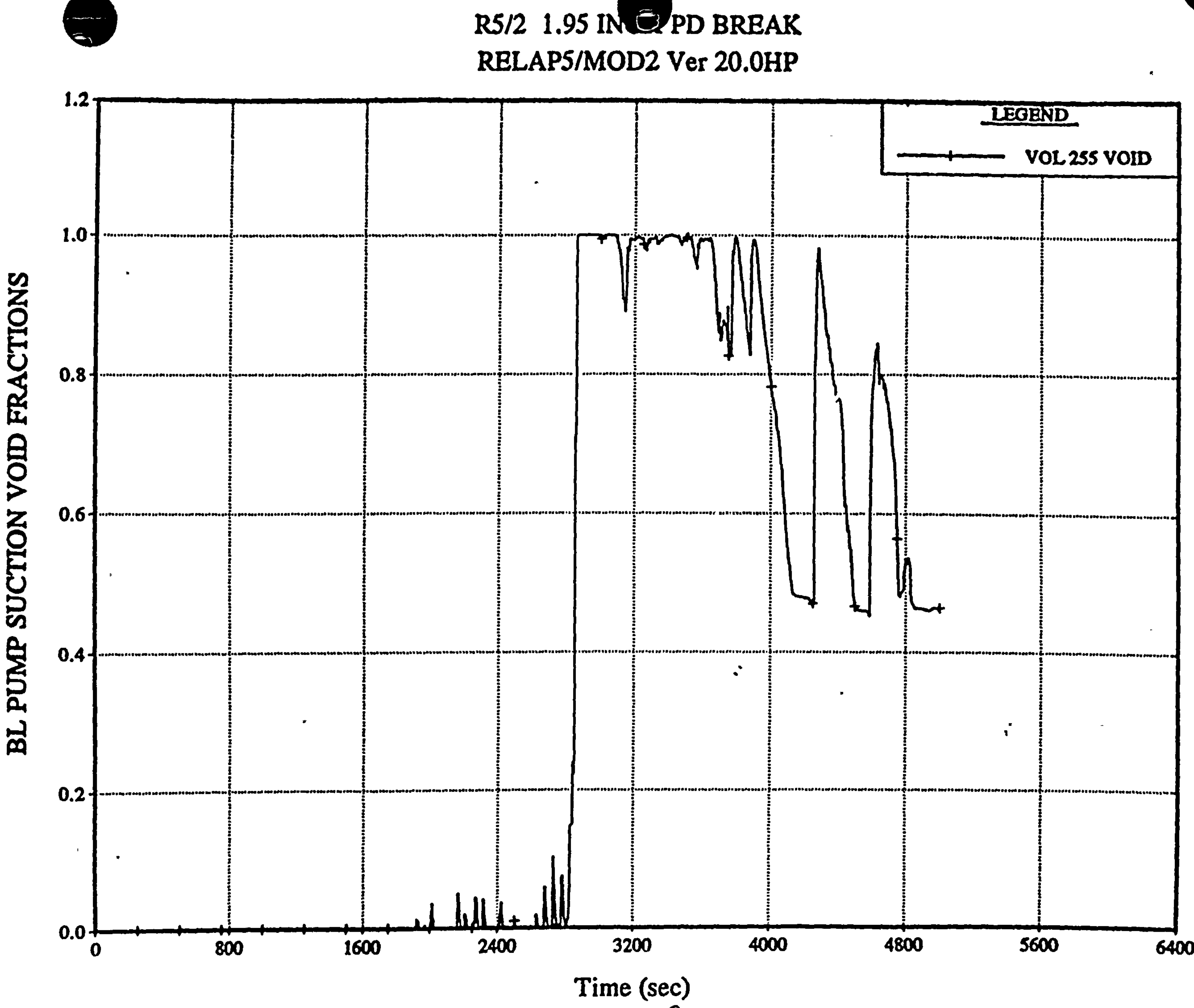


Fig - 8

12

Fig 8

FAJD_VOIDG005

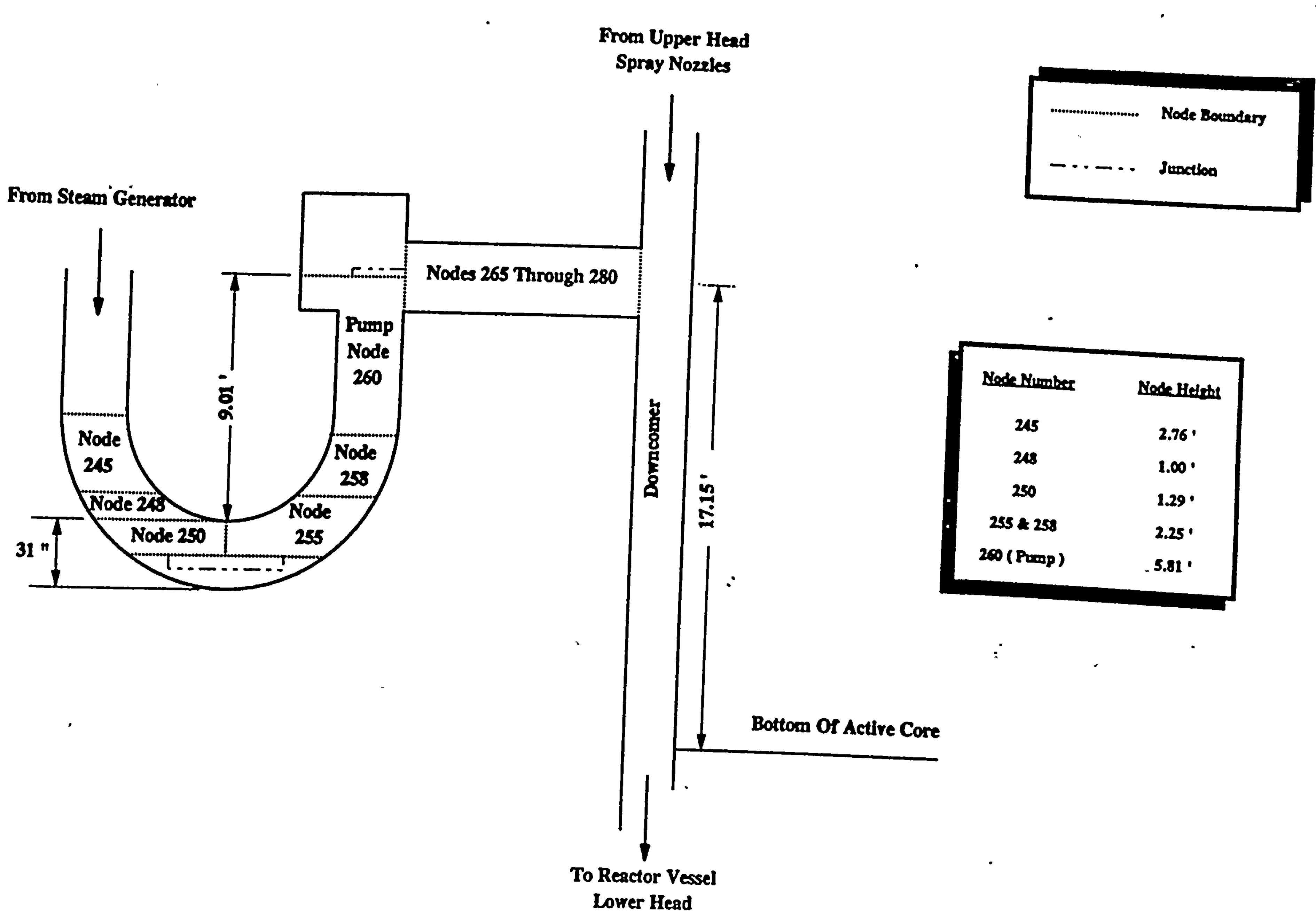


13 Fig - 9

Fig 9

FBDW VOIDG001

Figure 10: Typical FTI SBLOCA 4-[illegible]p RSG Pump Suction Noding Details

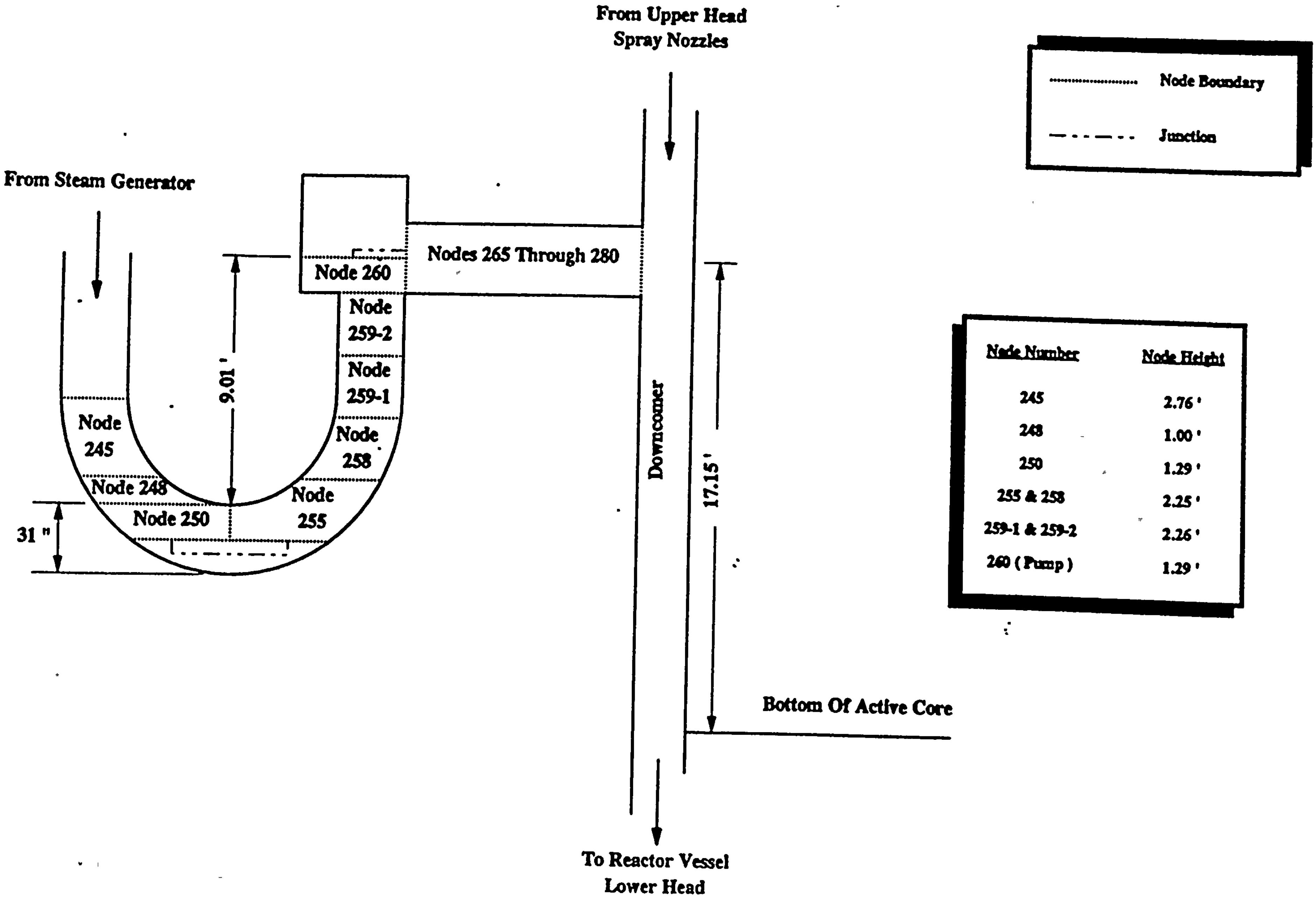


Node Number	Node Height
245	2.76 '
248	1.00 '
250	1.29 '
255 & 258	2.25 '
260 (Pump)	5.81 '

Fig-10

14

Figure 11: Revised FTI SBLOCA 4-Loop RSG Pump Suction Noding Details



Node Number	Node Height
245	2.76 '
248	1.00 '
250	1.29 '
255 & 258	2.25 '
259-1 & 259-2	2.26 '
260 (Pump)	1.29 '

Fig-11

15

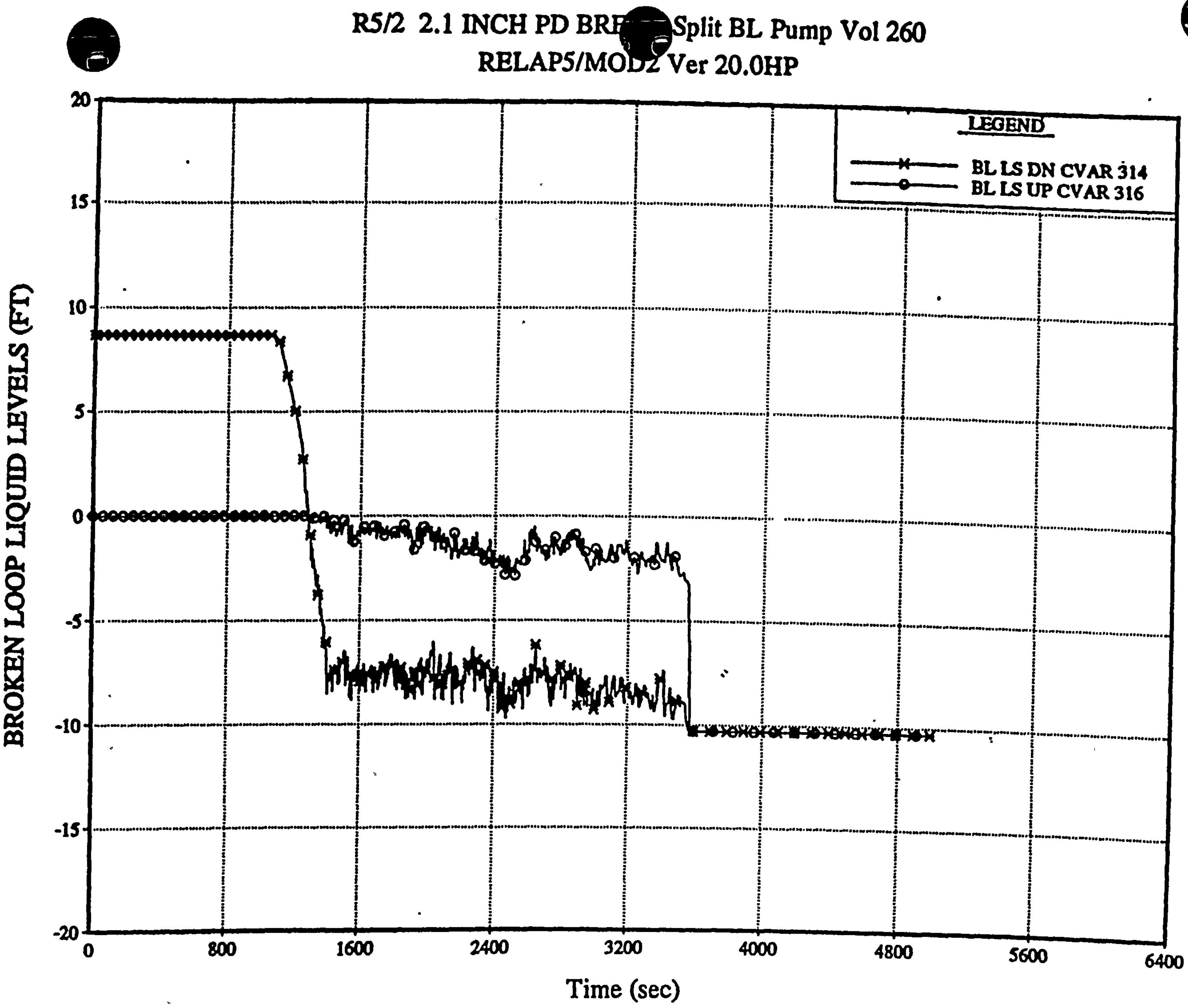


Fig-12

16

Fig 12

TATB VWRO_CNTRL040

R5/2 2.1 INCH PD BREAK Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP

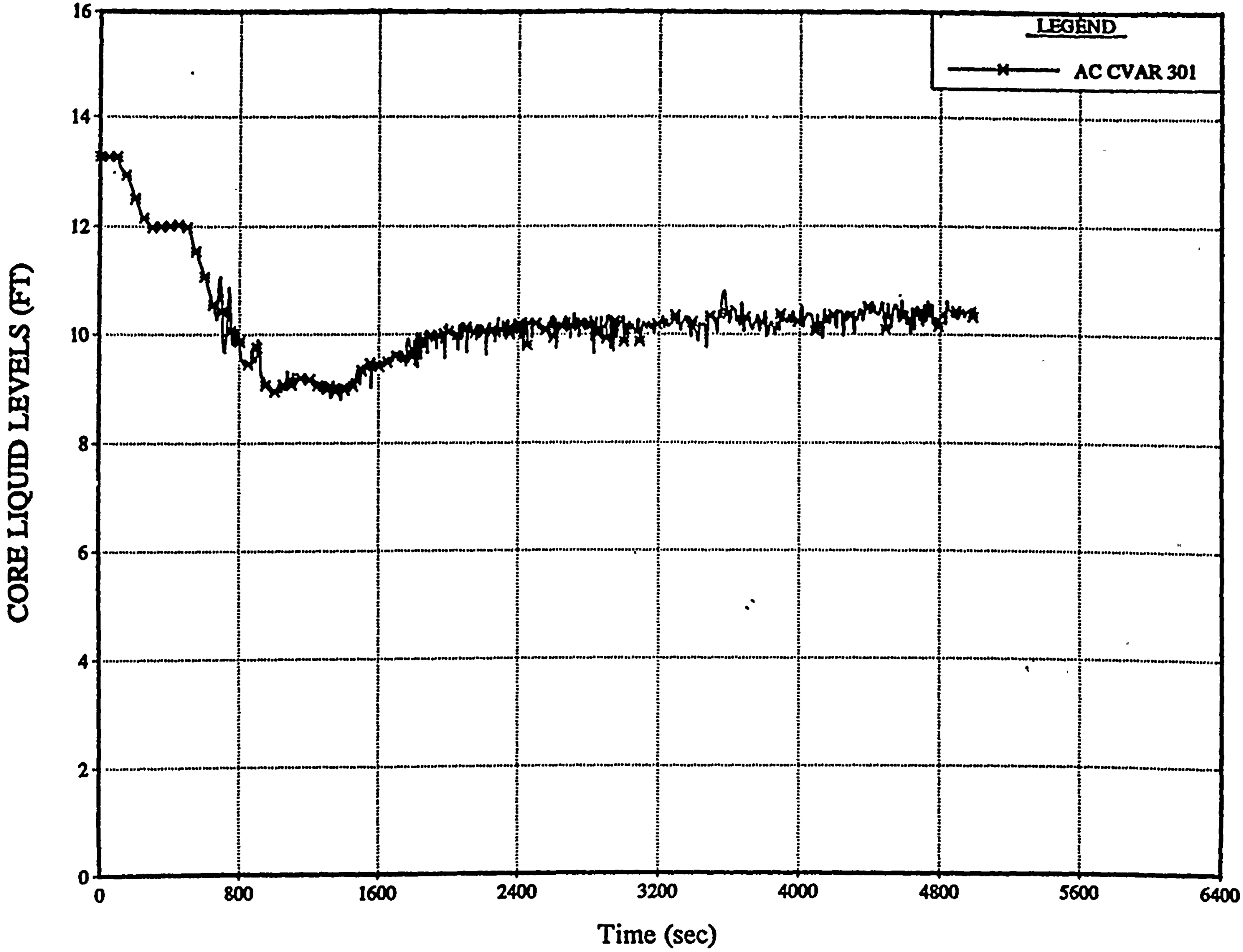


Fig-13

17

Fig.13

R5/2 2.1 INCH PD BREAK Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP

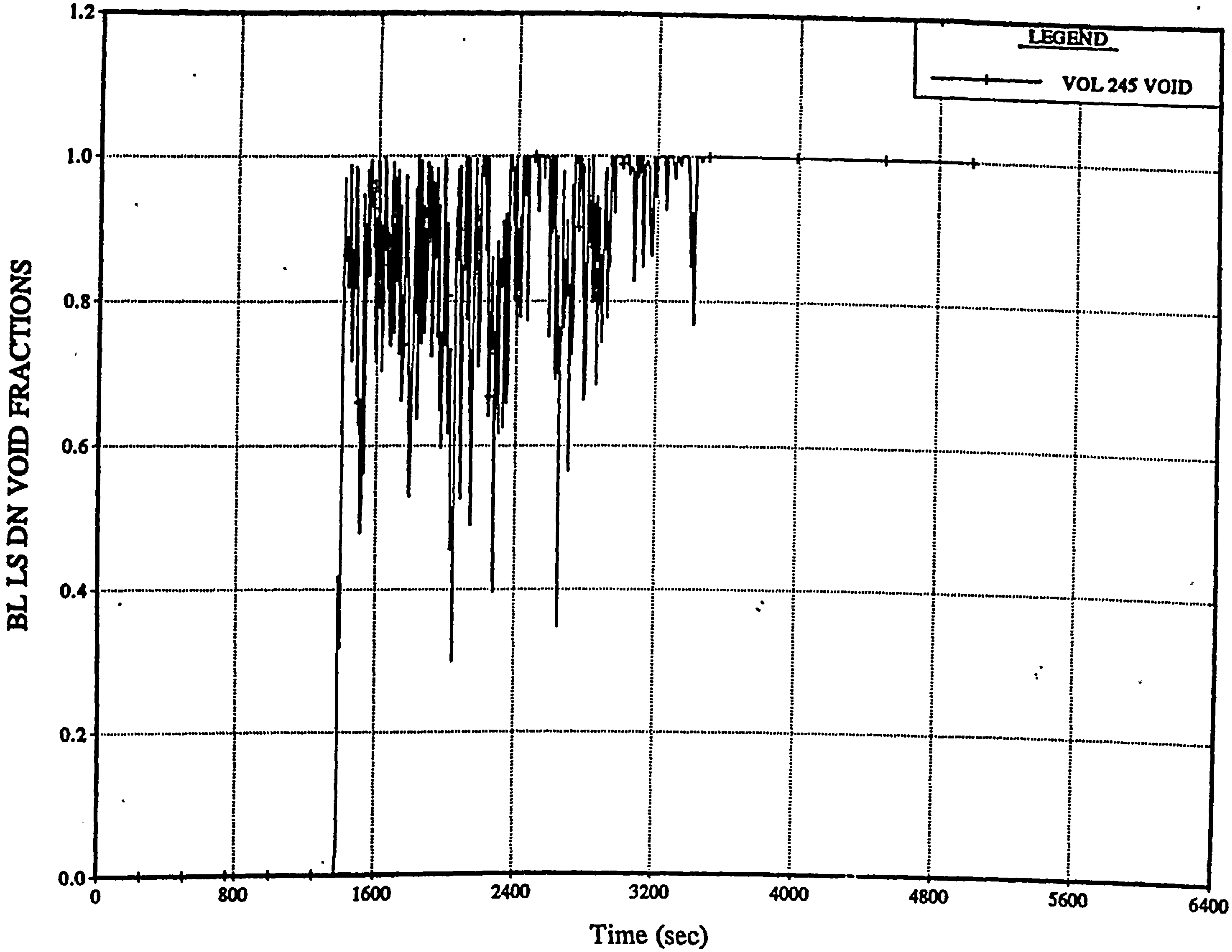


Fig - 14

18

Fig 14

TATB VWRO_VOIDG007

R5/2 2.1 INCH PD BREAK Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP

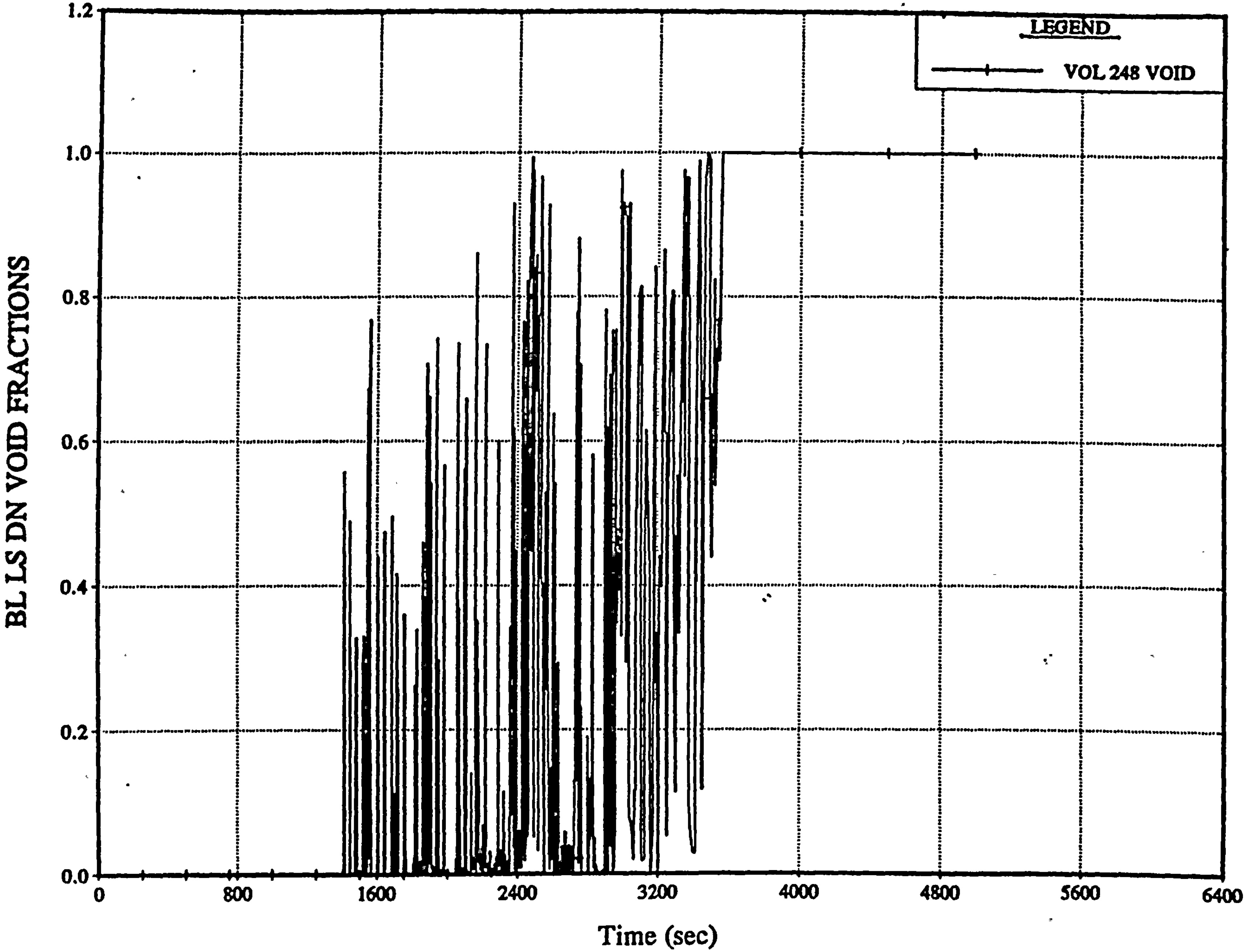


Fig 15

R5/2 2.1 INCH PD BR Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP

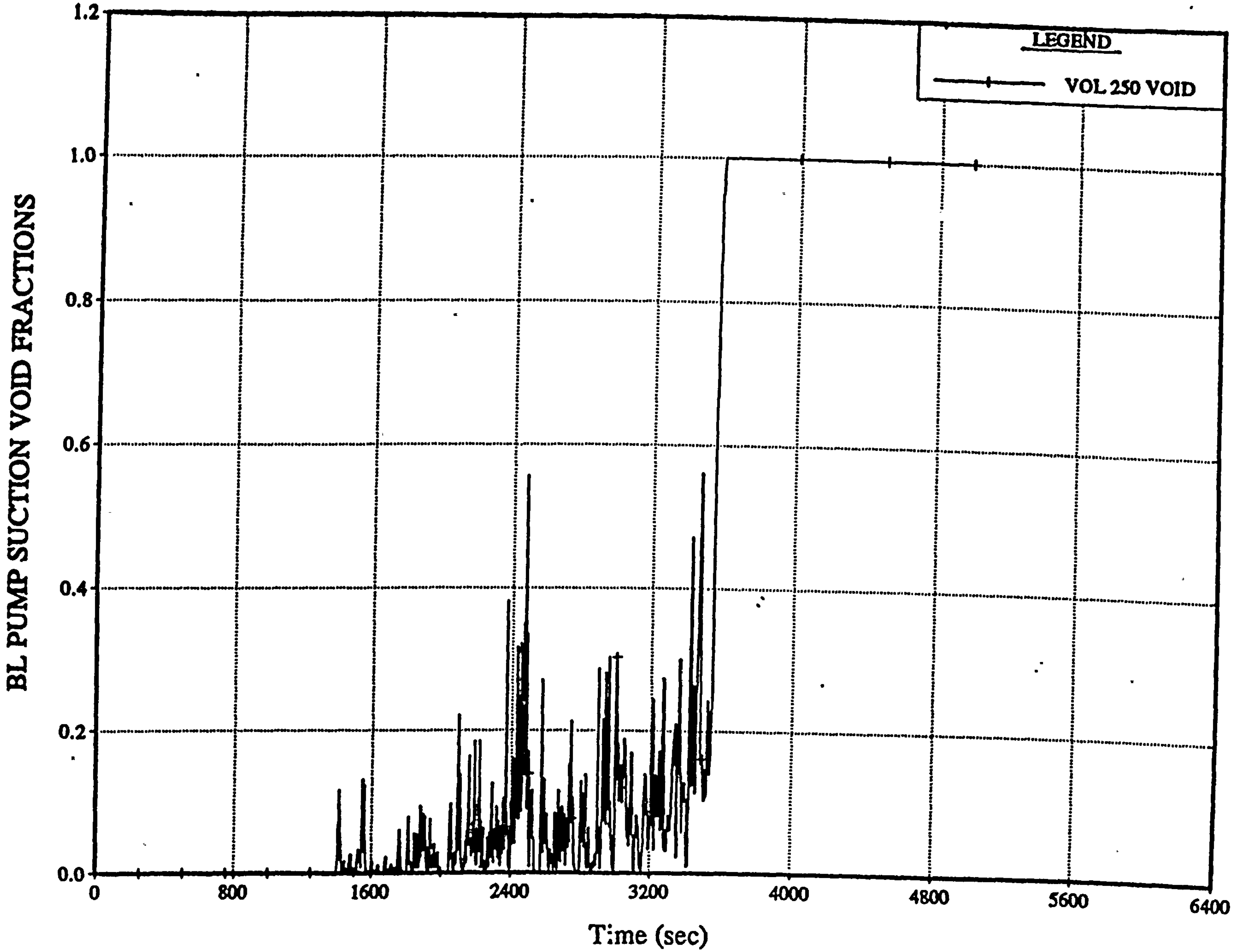


Fig 16

TATB VWRO VOIDG004

R5/2 2.1 INCH PD BREAK Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP

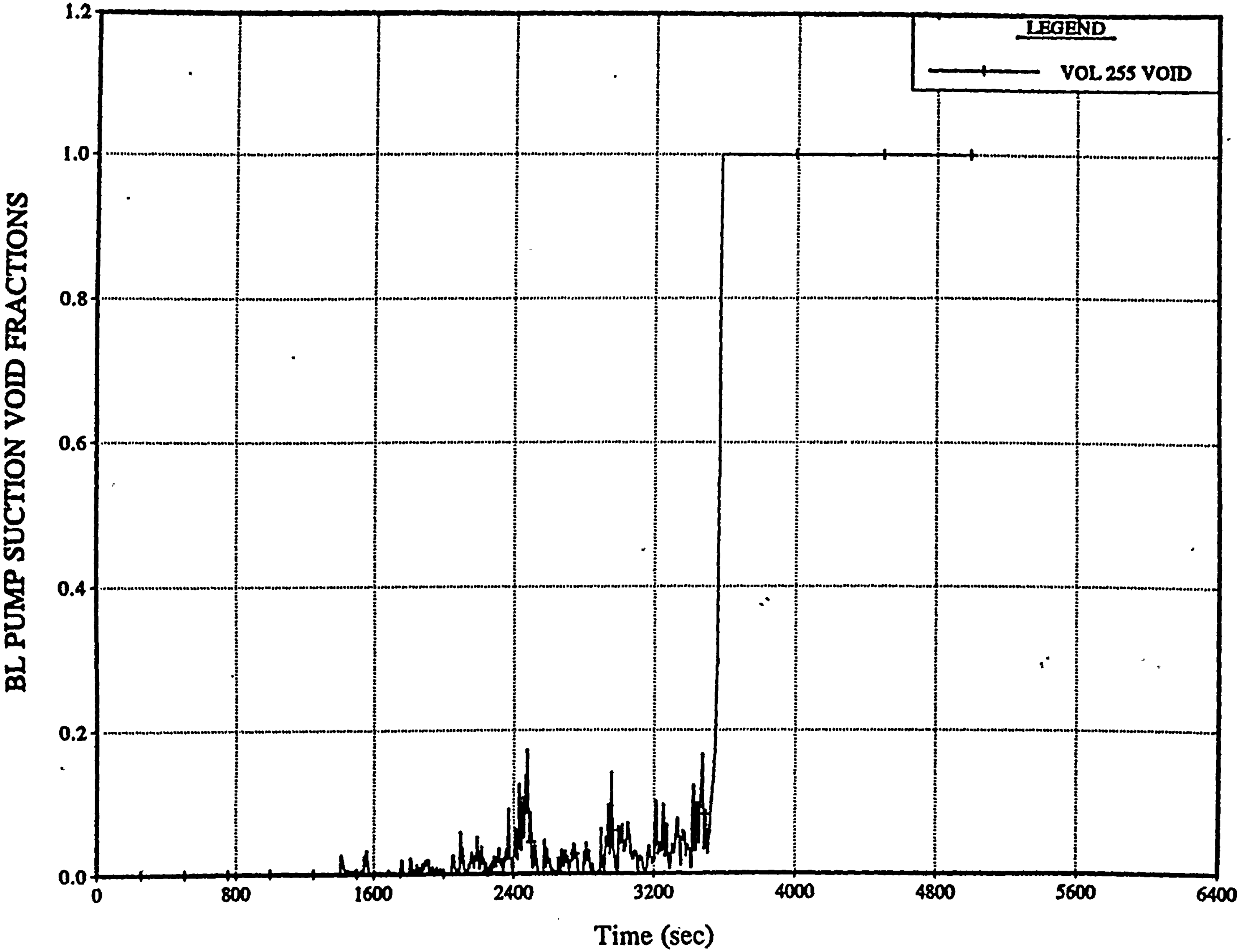


Fig 17

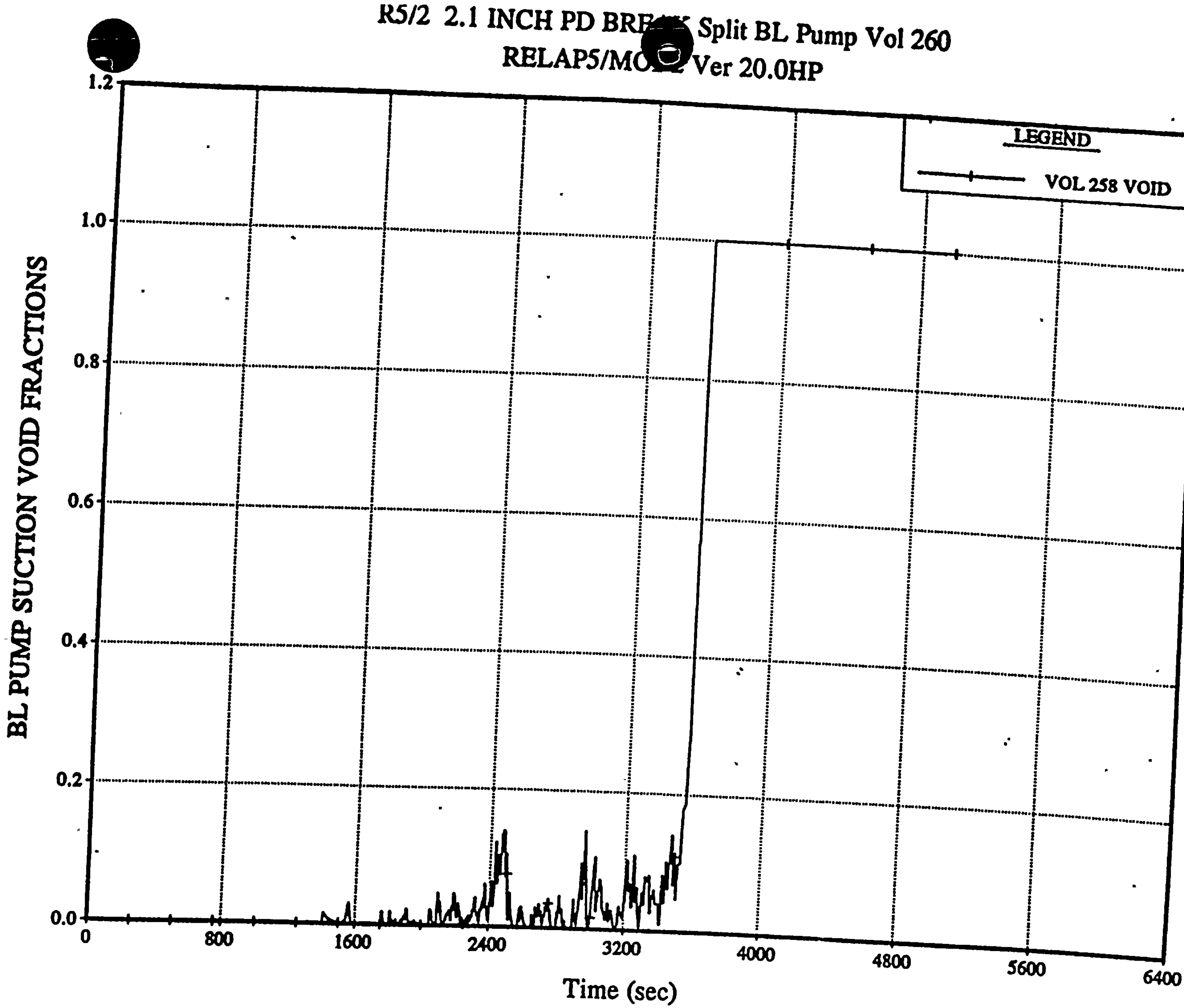
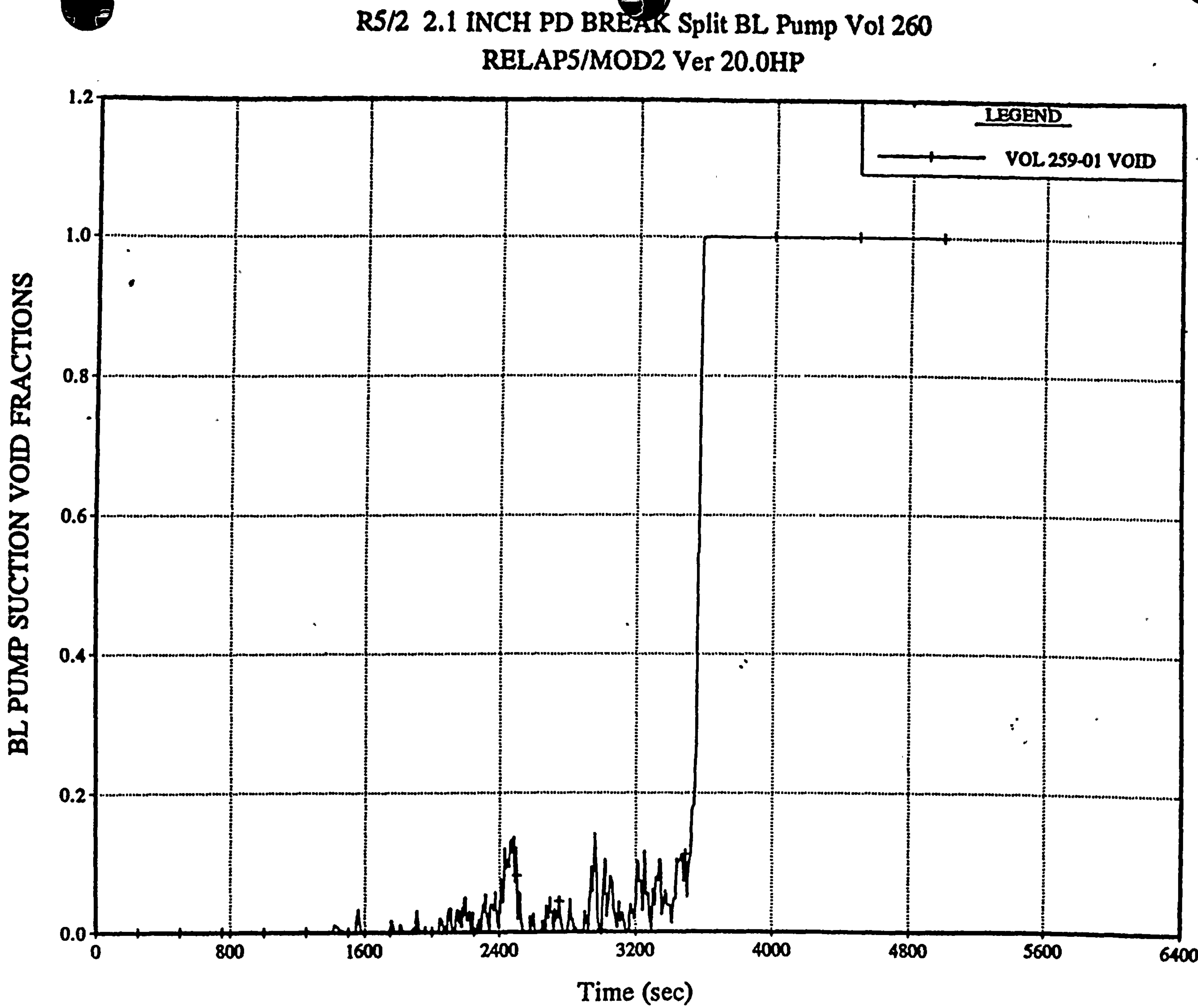


Fig 18

TATB VWRO_VOIDG002


R5/2 2.1 INCH PD BREAK Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP
LEGEND
VOL 259-01 VOID
BL PUMP SUCTION VOID FRACTIONS
Time (sec)
0.0
0.2
0.4
0.6
0.8
1.0
1.2
0
800
1600
2400
3200
4000
4800
5600
6400

R5/2 2.1 INCH PD BR[illegible] Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP

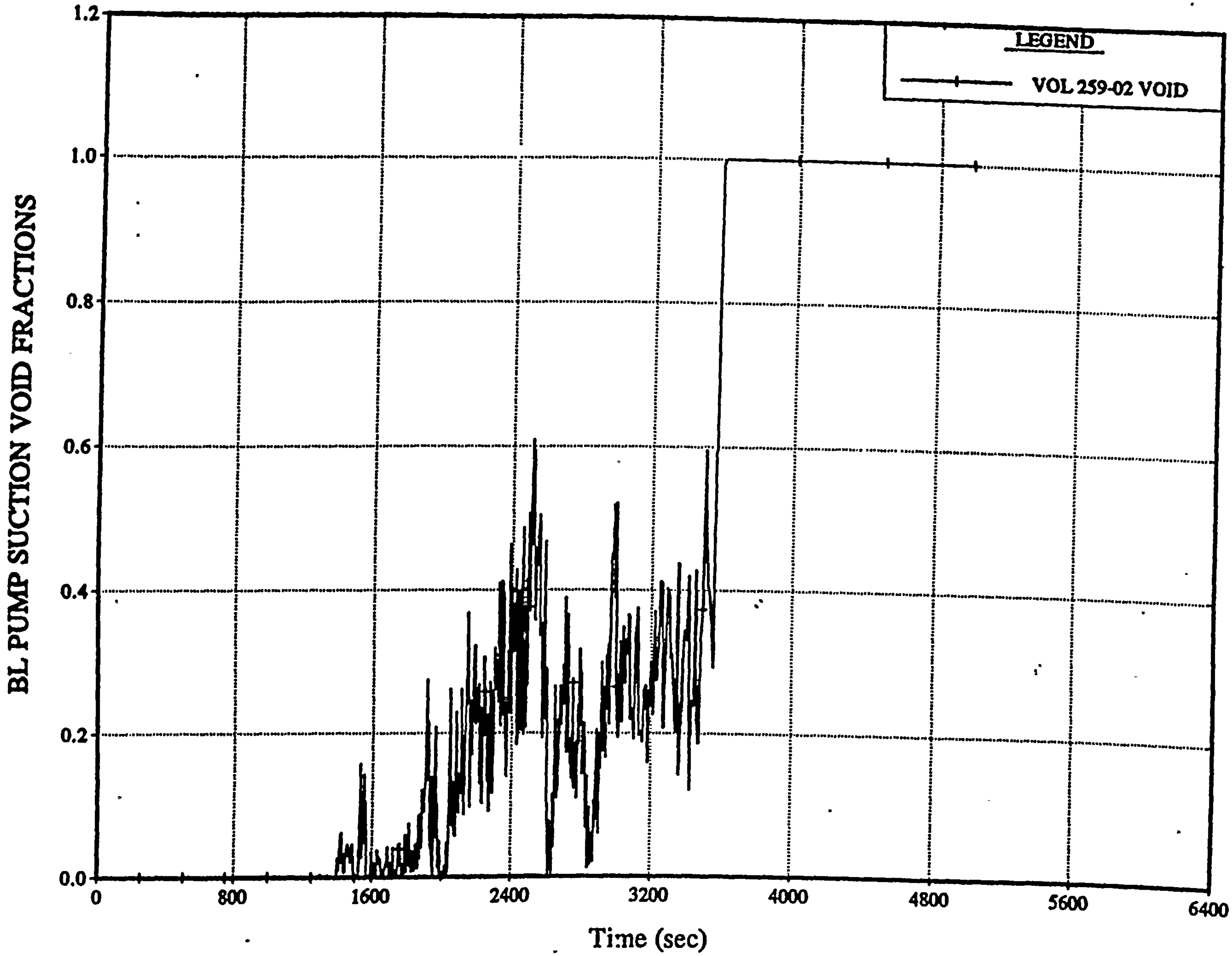


Fig 20

TATB VWRO VOIDG009

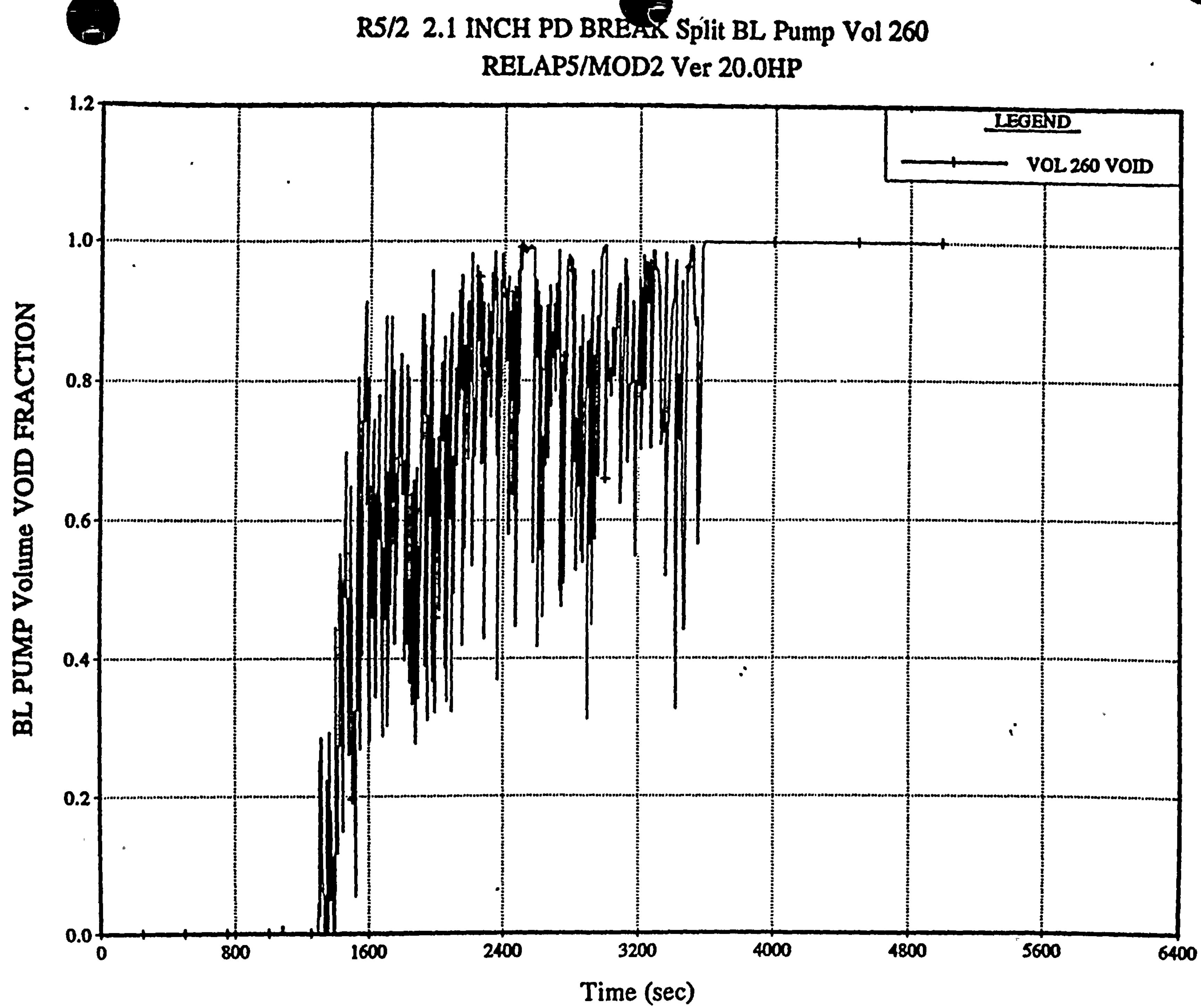

R5/2 2.1 INCH PD BREAK Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP
LEGEND
VOL 260 VOID
BL PUMP Volume VOID FRACTION
Time (sec)
0.0
0.2
0.4
0.6
0.8
1.0
1.2
0
800
1600
2400
3200
4000
4800
5600
6400

Fig 21

R5/2 2.1 INCH PD BR[illegible] Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP

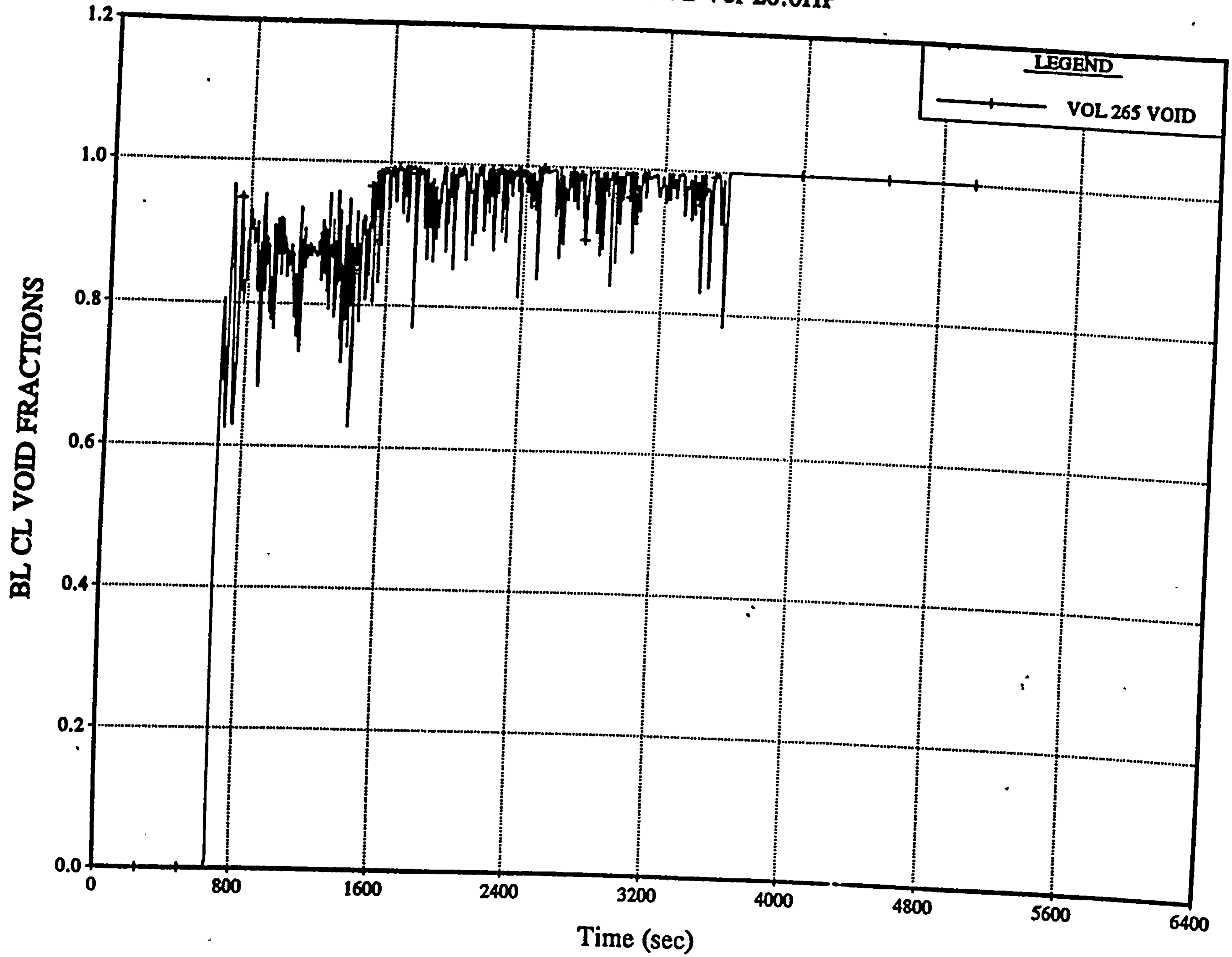


Fig 22

TATB VWRO VOIDG005

Break Orientation: The break orientation, for SBLOCA studies, is placed at the bottom of the cold leg piping, between the ECCS injection location and the reactor vessel, since this configuration poses the greatest challenge to the ECCS in providing sufficient coolant flow to maintain core cooling. With the break so situated, ECCS entering the RCS through the injection nozzle in the broken cold leg must pass over the break prior to penetrating the reactor vessel. Unless the pump discharge piping is already full, the emergency coolant will be passed out of the break, unable to provide core cooling. This limits the effective ECCS flow, during critical cooling times, to that injected into the remaining loops (intact loops). For that reason, most plants have limits on the amount of injection that can be delivered to any one loop or leg during SBLOCA. A typical limit is that no more than 70 percent of the total ECCS flow can be delivered to any one injection nozzle.

The issues involved with the evolution of SBLOCA transients having alternate break orientations are primarily concerned with the longer term management of the accident than with the measurement of the capability of the ECCS system to provide sufficient and timely injection. The investigation of an SBLOCA scenario with the break at the top of the pump discharge piping is illustrative. For the first period of the transient--reactor trip, ECCS initiation, and loop draining through loop seal clearing--the LOCA is essentially the same irrespective of the break orientation, top, side, or bottom. The pump discharge piping is essentially full of water. Plant pressure is controlled by a balance between the volumetric discharge through the break, the vapor generation in the core, and condensation in the steam generator, if that is needed. Plant inventory is being lost rapidly and a liquid level imbalance is being setup between the downcomer and the core in order to achieve loop seal clearing. Loop seal clearing, when it occurs, is self advancing and rapid. At the end of loop seal clearing, one or more loops have been cleared of liquid; the liquid is retained in the core and downcomer. The downcomer core level imbalance is reduced to that necessary to drive steam to the break. This process, though dependent on break size, is independent of break orientation; it occurs in essentially the way same for bottom, top, and side breaks. Some arguments exist that side and top breaks offer less potential for liquid diversion to the break during loop seal clearing and, thus, arrive at a stable cleared configuration with higher vessel inventories than do bottom breaks. That effect, however, is difficult to demonstrate.

Following loop seal clearing, the ECCS system is challenged as to its ability to supply water at a rate sufficient to replace the water that is being boiled off in the core. In the critical cases, with a single failure of one of the high pressure injection systems (HPIs) and the break located at the bottom of the discharge piping, the ECCS cannot immediately keep pace with core boiling. The system is then in a boildown mode. The inventory in the reactor vessel continuously decreases until the decay heat drops or the ECCS flow increases (because of system depressurization) to the point of achieving a match with the core boiling. If the imbalance is sufficient, the core may uncover, exposing its upper regions to steam cooling before the match occurs. Modeling this phase of the transient with a bottom break is limiting because top or side breaks have effective ECC flows, that are up to 40 percent higher. Thus, for the initial system response and the determination of the adequacy of the ECCS, the bottom break is clearly the conservative choice.

After this initial period, some differences in the modes of accident recovery do occur. Following the acceptable match of decay heat and ECCS flow, the decay heating will continue to decrease at a slow rate; the system pressure may also continue to slowly decrease. This will create excess ECCS and the reactor vessel will start to refill. The rate is dependent on the particulars of the accident and can vary from a reasonable refill rate to an extremely slow one. Eventually the downcomer will be refilled with ECCS water backing up into the discharge piping. At this point, the behavior of the bottom, and side and top breaks starts to differ. For bottom breaks, the liquid backing up into the discharge piping will result in a fluid quality change at the break such that the break discharge is sufficient to remove excess injection. The downcomer remains full; the core, being hydrostatically balanced against the downcomer, is well covered and nothing of significance occurs for an extended period of time. For a side or top break, the break flow cannot respond to the rising system water level and the excess ECCS eventually spills over into the pump suction piping. Whether the loop seals reform or not and the consequences of that happening depend on many factors including operator action to manage the accident.

That the plant is safe and can be managed acceptably during recovery is, in FTI's view, a concern for the plant Emergency Operating Procedures (EOPs) or other devices that control the eventual recovery of the plant. The initial response of the ECCS, its adequate sizing, and the establishment of long-term cooling have, by this phase of the accident, been established. That is the purpose of 10CFR50.46. The eventual recovery from the accident, the evaluation of the multiplicity of operator actions, and their affect on the RCS and core are operational matters. Furthermore, these evaluations should be conducted with realistic boundary conditions such that expected and probable plant behavior is described; aberrant, supposedly conservative assumptions, should not be used. Still an investigation into the possibilities can be useful in determining if any role remains for LOCA analysis past the initial ECCS response.

There are four main factors that determine the continued course of an SBLOCA for side and top breaks. Actually, even a bottom break will eventually evolve to the same configuration as side and top breaks since the break flow cannot be adjusted infinitely, but their development requires an extremely long time period. For our purposes, it is sufficient to consider just the top or side break. The main factors are:

a. The amount of steam flow possible through the upper head spray nozzles (UHSNs).

   This vent path, if it supports the core steaming rate, can eliminate the need for steam venting through the loops. Because core steaming is dependant on decay heat, the UHSNs increase in significance as time progresses.

b. The amount of steam or water that can be passed through the reactor vessel fit up leakage.

   Hot side to cold side leakage is another vent path capable of eliminating or reducing the need for loop venting. This mechanism responds with time in two ways. First, decay heat decreases with time reducing the amount of steam to be vented and, secondly, the RCS nominal temperature also decreases with time, increasing the fitting gaps and improving vent capability. Care should be exercised in applying leakage credit during partial core

uncovery since the steam in the upper head will be superheated, tending to heat the metal structures and reduce the gaps.

c. Whether the mechanism for filling the suction lines evolves gradually or it is a spontaneous development.

If the means for spilling water into the suction piping is the decrease in decay heat, the build up of excess injection will occur slowly and the accumulation of water in the suction piping will be gradual. The potential for blockage will be imposed gradually and at times beyond which loop venting may not be needed. If, however, the increase in spillage is rapid, as may occur because of the return to service of a failed injection system, the potential for blockage can occur with reasonable rapidity.

d. The amount of steam flowing through the loops that is not condensed in the steam generators.

This of course is the most direct factor of concern in evaluating the effect of re-closure of the loop seals. An important consideration is the degree of management credited. If the steam generator pressure control is conducted as intended by the EOPs, the plant will evolve to a reflux mode with no need for loop venting except where spontaneous increases in injection flow occur (item c).

Depending on the plant, the UHSNs can eliminate any concern over a secondary loop seal clearing process. All Westinghouse plants, classified as T_{cold} upper head plants, have reasonably large UHSNs. McGuire/Catawba and Sequoyah are examples of such plants. An examination of the Sequoyah calculations for a 1.9-inch break shows that the process of loop seal clearing is interrupted at about 2,000 seconds by the development of a head imbalance between the downcomer and the core that is large enough to support sufficient steam flow through the UHSNs to eliminate the need for loop venting. For this break and breaks of smaller cross-sectional areas, the loops never clear and, after achieving a minimum suction piping downside level, the suction piping will gradually refill. Because the core swell factor (mixture level divided by the collapsed level) is approximately proportional to core steam generation and the differential pressure required for flow through the UHSNs is proportional to the square of the rate of steam generation, the elevation head difference between the core and the downcomer will decrease more rapidly than the swell height difference as decay heat drops. The core mixture level actually increases with time, assuring continued core cooling. Therefore, for breaks that do not require loop seal clearing during the initial system response, no need for clearing will develop later in the accident. Further, for larger breaks that do require loop seal clearing, the ability to flow sufficient steam through the UHSNs will develop with time, also eliminating the need for loop steam venting. Thus. for T_{cold} upper head plants, because the UHSNs have substantial capability for steam venting, no concern over the refilling of the loop seals with time exists.

For T_{hot} upper head plants, the UHSNs are not sufficient to vent a meaningful amount of steam. Such plants can be bounded by considering the results of excess ECCS for a theoretical plant, absent UHSNs and internals leakage. To this end, an evaluation has been conducted for a plant

without UHSNs or internals leakage and for which no operator actions have been taken to manage the accident. The analysis comprises an examination of the potential condition of the RCS following a 2-inch diameter break in the side or top of the cold leg just after loop seal clearing, 1½ hours into the accident, and at six hours into the accident. In each case, sufficient time has elapsed for the suction piping to have been refilled to the extent predicted. The plant is considered to be in a transient mode for the evaluation of the conditions post-loop seal clearing and in a quasi-steady-state for the evaluations at 1½ and six hours. The spectrum of conditions considered are one and four loops venting and one or two HPIs providing makeup. No injection is arbitrarily lost or spilled from the system. The timing of loop seal clearing was obtained from available spectrum calculations performed with the evaluation model. The timing may differ slightly for a top break with two HPIs, but that is not a significant simplification.

One key in understanding the analysis is to realize that a transport mechanism for the core energy must exist. Either the core is boiling and steam is being used to transport energy to the break or the RCS is basically water solid and experiencing natural circulation. A water solid configuration at six hours is possible, if the operator has followed the EOPs and depressurized the steam generators. However, there is no concern for loop seal blockage in a circulating system so that case will not be considered further. Because steam is the transport mechanism, the core is boiling and the flow rate of water to the core can be determined by balancing the heads between the suction riser section and the core given that the inlet enthalpy is specified. For this evaluation, the core inlet enthalpy was assumed to be the injection enthalpy and a level credit was taken for the difference in the downcomer liquid density and the core average liquid density. An analogous assumption, that the core inlet is saturated, can be made with no density difference applied between the core and the downcomer. Either approach achieves essentially the same core mixture level. One depresses the core collapsed level less, while the other generates a higher mixture swell. Steam generated in the core passes through one or four loops and is mixed with liquid in the pump suction riser section at the spill under. Here, excess ECCS subcooling condenses steam to the extent possible and any remaining non-condensed steam is bubbled up through the riser section to the break. For the post-loop seal clearing analysis, the pressure is taken from the reference RELAP5 calculation. For the extended time evaluations, the pressure is determined from the break model (Moody or Extended Henry-Fauske) and the consideration of mass and energy equilibrium for the RCS. For the single HPI cases, the break requires steam and water to be in equilibrium and only that steam flow (the break steam) was used to lighten (decreased density) the riser section. For the two HPI cases, the HPI sensible heat was sufficient to absorb all of the core heat and no break steam flow occurred. In these cases, the condensation process in the bottom of the riser section was assumed to take place in an exponential pattern over the bottom four feet of the riser section. Forty-eight percent of the steam was condensed in the first one-half foot, eighty percent was condensed by 1½ feet, and all the steam was condensed by four feet.

The table presents the results obtained for liquid collapsed levels in the riser sections of the venting suction piping and the reactor core. The table also indicates whether or not the core is covered by the boiling mixture. As can be seen from the table, the core is essentially covered with a boiling mixture for all cases. The one HPI, four-loop venting case has a core mixture height of 11.9 feet at six hours, which is considered essentially covered. Extending these results

to greater times will eventually demonstrate core uncovery. However, operator action in conjunction with the EOPs has been delayed for over 5 hours for these analyses. Because such action will mitigate the consequences of these transients, it is not necessary to consider the response of the system for longer times.

The evaluations provided are appropriate if the processes described and credited are not erratic. That may not be true for the condensation process in the riser sections. At that location, with steam being forced into subcooled water, water cannon or water hammer effects may be produced. In that event, the system can be expected to vary about the nominal conditions derived here. Core mixture levels will be both higher and lower than those indicated, but, because the core heating at these times is not rapid, the core overall should be well cooled. Again, if the operator follows the EOPs, the potential for these conditions will be removed early in the event.

In summary, FTI maintains that the decision to run 10CFR50.46 calculations for breaks at the bottom of the piping is appropriate. These breaks clearly offer the greatest challenge to the emergency core cooling systems. SBLOCA transients may evolve differently for top and side breaks than for bottom breaks, but the evolution is essentially independent of the ECCS. Further, the differences occur during the period of accident management that is the purview of the Emergency Operating Procedures and they should not be evaluated with the required EM conservatisms. Not withstanding these considerations, FTI has considered the evolution of top and side breaks. For T_{cold} upper head plants, the evolution of the transient has been shown to produce a smooth increase of core coolant level with sustained and continuous core coverage after a possible initial uncovery. For T_{hot} upper head plants, inter-vessel leakage around the hot leg nozzles serves the same purpose as UHSNs for the T_{cold} plants, making long-term cooling a smooth process with no core uncovery.

Additionally, top breaks were evaluated out to 6 hours for a plant without UHSNs or inter-vessel leakage. It was shown that, at least on the average, the core will be continuously covered. It was demonstrated that the transient can progress past six hours without experiencing serious core uncovery, requiring many additional hours to produce significant core uncovery. Because the potential to require loop venting in the long term is limited (UHSNs and inter-vessel leakage effects) and because the EOPs typically recommend operations to depressurize the plant early in the transient, thereby refilling the plant and mitigating any need for loop venting, FTI believes that any consideration of times beyond those presented to be the proper subject of operational procedures and not suited for consideration under 10CFR50.46.

Analysis Results for a 2-inch Diameter Pump Discharge Break at the Top of the Pipe

Time hours	Decay Heat %	HPIs Operating	Loops Venting	Pump Suction Riser Collapsed Level feet	Core Collapsed Level feet	Core Mixture Level feet
0.5	2.0	1	1	2.4	14.6	12+
			4	5.5	11.5	12+
		2	1	3.7	13.3	12+
			4	6.2	10.8	12+
1.5	1.5	1	1	4.4	12.6	12+
			4	6.6	10.5	12+
		2	1	7.4	11	12+
			4	8.2	10.2	12+
6.0	1.0	1	1	5.9	11.1	12+
			4	7.3	9.7	11.9
		2	1	7.6	10.4	12+
			4	8.2	9.8	12+

Cross Flow Resistance and Core Modeling: In our 3/28/96 telecon, questions were raised as to the basis for the crossflow modeling used within the core. The modeling is outlined in Section 4.3.2.5 of volume II of the RSG evaluation model report, BAW-10168, Revision 2. Basically, the model is a 20 axial region core, radially divided into a single assembly hot channel and the remainder of the core. Each volume in the core model is connected vertically and horizontally. Vertical resistance is based on core design factors which in turn are based on flow tests for the fuel assemblies. Correlations for the prediction of lateral resistances vary substantially. A k-factor value of 2, based on the interface area between adjacent fuel assemblies, has been selected for the evaluation model. This value produces reasonable results that agree with experimental expectations for SBLOCA. The value, however, does not appear to be unique and either smaller or larger values would also appear to produce valid results. The B&W-designed plant RELAP5 small break evaluation model uses a value of 200 for the base crossflow resistance and does not produce substantially differing predictions. (There are indications, however, that the higher resistance used in the B&W-designed plant SBLOCA model may have a stableizing influence on the calculation.)

Two adjustments are imposed on the basic resistance in order to assure conservative SBLOCA predictions. For the top half of the core, the flow resistance from the average channel to the hot channel is increased by a factor of 10 (flow resistance from the hot channel to the average channel is not increased). This has little effect on the behavior of the core mixture or the core flows below the mixture level. However, above the mixture in the steam cooling region, provided the core has uncovered, the increased resistance limits any tendency to flow steam from the average to the hot channel. It is expected that steam will flow from the hot channel to the average because of the higher vapor generation in the hot channel. Because flow diversion out of the hot channel is a conservatism, that flow is not impeded. However, flow reversion back to the hot channel would have the effect of reducing the hot channel vapor temperature and increasing cooling. Although some flow reversion is expected, the resistance within the model is increased so as to limit the effect. The factor is only applied to the upper half of the core because, on a practical basis, it is not possible to predict acceptable cladding temperatures if the top half of the core is uncovered for an extended period. This modeling adjustment, then, is taken to help assure a conservative evaluation.

For reasons similar to the increased crossflow resistance, the hot channel outlet reverse flow resistance was increased to a k-factor of 200 based on the assembly flow area. It was envisioned that this would reduce the tendency for liquid fall back into the hot channel by encouraging liquid to flow into the average channel and then crossflow to the hot channel. The effectiveness of the high reverse flow resistance, however, is mitigated by the need for the hydraulic solution to achieve a pressure balance between the inlet and outlet plenums. As the flows and void fractions develop axially within the core, the hot channel maintains a slightly increased voiding because of its higher vapor generation rates. This leads to an apparent pressure imbalance between the two columns (hot and average channels) as the core exit is approached. To adjust for this imbalance, the solution allows negative liquid flow into the uppermost volume of the hot channel creating a lower void fraction for that volume. The reduced voiding in the upper volume balances the channel pressures. Note should be taken that the upper two volumes of the hot or average

channels do not represent nuclearly heated regions of the plant. These volumes model the upper unpowered segments of the fuel pins (the fuel pin upper plenum and interior springs) and the upper nozzle of the fuel assembly. Thus, the flow and the void reduction do not occur within the core active region. The resultant negative flow from the upper plenum to the hot channel exit volume only occurs when the upper plenum contains some mixture. Model prediction problems are not created because once the inner vessel mixture level falls into the core region the pressure balance is maintained by a slightly increased mixture level in the hot channel. This higher mixture level in the hot channel is physically real and well modeled. Observations of the core mixture level predictions for the hot and average channel discussed below demonstrate the credibility of the solution. The increased resistance has been maintained in the model as a hedge against possible core reverse flow. The resistance does not work as a flow diversion under stagnate conditions but is likely to divert flow away from the hot channel under flow conditions. This would be a meaningful conservatism if SBLOCA were to involve any substantial period of reverse core flow. Although no such period can be identified, the only reverse core flow phases are those occurring during the loop stagnate phases of loop seal clearing and core boildown, the increased resistance factor has been kept as a precaution.

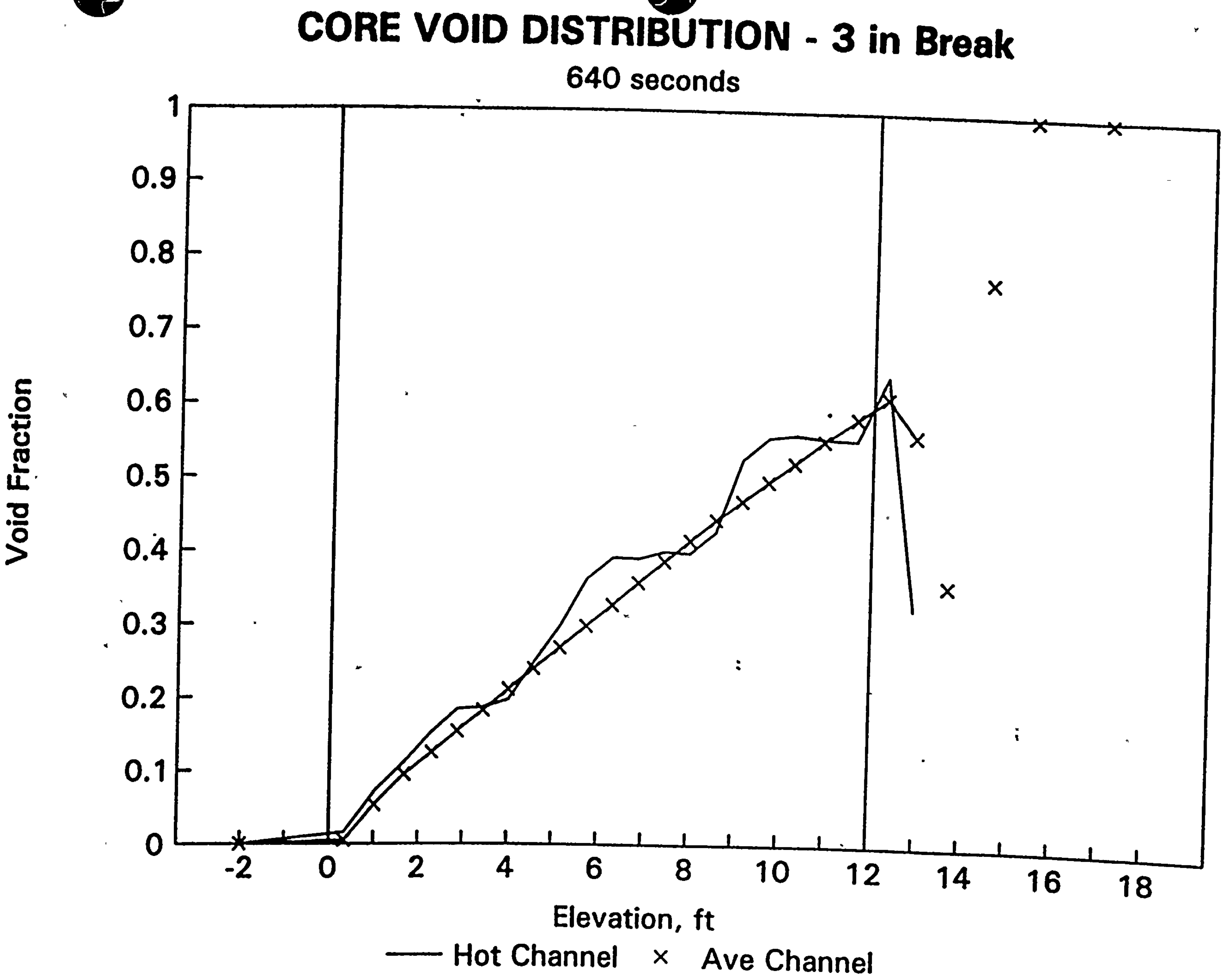
That the hot channel and average channel mixture heights evolve reasonably during a core uncovery can be observed in the attached figures. These figures display the axial void distributions of the hot and average channels as they developed for a 3-inch pump discharge break in a Westinghouse-designed 4-loop plant over the loop seal clearing period. The figures display void fraction versus axial core elevation from the lower plenum to the upper plenum at the elevation of the outlet nozzles. Each void fraction is displayed axially at the center of the volume from which it is taken and is connected to the void fraction of the adjacent nodes by a straight line. If not recognized, this technique can introduce some confusion, as occurs between the lower plenum and the core. The lower plenum is or is nearly liquid solid throughout the time period of these graphs, but the linear connection to the first core volume, which is legitimately voided, produces a visual impression that the lower plenum contains steam as the bottom of the core is approached. In truth there is a step change in void content between the lower plenum and the core. The same recognition should be made in reviewing the upper plenum void fractions. This, in part, is the reason that the channels, except for the lower plenum to the core, are displayed with connecting lines while the upper plenum volumes are displayed as points. The time at which the figure is captured is displayed just above the figure border. Within the upper plenum, the upper most value is at the elevation of the center of the core outlet nozzles. This volume spans the height of the outlet nozzles. The next lower volume is entirely below the span of the hot leg piping.

Loop seal clearing for the case shown in the figures occurs at approximately 715 seconds. The graphs display the core elevation head/mixture height as the necessary head to clear the loop seal develops on the approach to loop seal clearing and as the core refills after clearing. Graphs are provided at 640, 660, 680, 690, 700, 710, 715, 720, and 800 seconds. By 640 seconds, the clearing process has initiated and the core mixture level has fallen below the nozzle belt as indicated by the void fraction in the upper most volumes. (The upper volume represents the portion of the upper plenum adjacent to the outlet nozzles.) The core is still covered with mixture and the depressed void fraction at the exit to the hot channel can be observed. It can also be

observed that the correspondence in void content between the average channel and the hot channel is quite good. Deviations occur, but the general trend is a slightly higher void content in the hot channel. There is no indication that the lower void content of the hot channel exit volume has propagated downward. By 660 seconds, more of the upper plenum is voided, but the core is still covered and the core void distributions remain reasonable. At 680 seconds, the columns representing the hot and average channels are starting to void. The upper plenum is essentially 100 percent voided. The core heated regions are still covered since the high voiding has not penetrated below the non-heated regions of the fuel assemblies. By this time, before any core heatup, the void fraction for the hot assembly upper region has evolved into agreement with that of the average channel. At 690 seconds, the heated regions of the hot and average channels have started to uncover. Loop seal clearing is now about 25 seconds away. Because the core outlet void fraction is at 90 percent, the cladding temperatures remain near saturation.

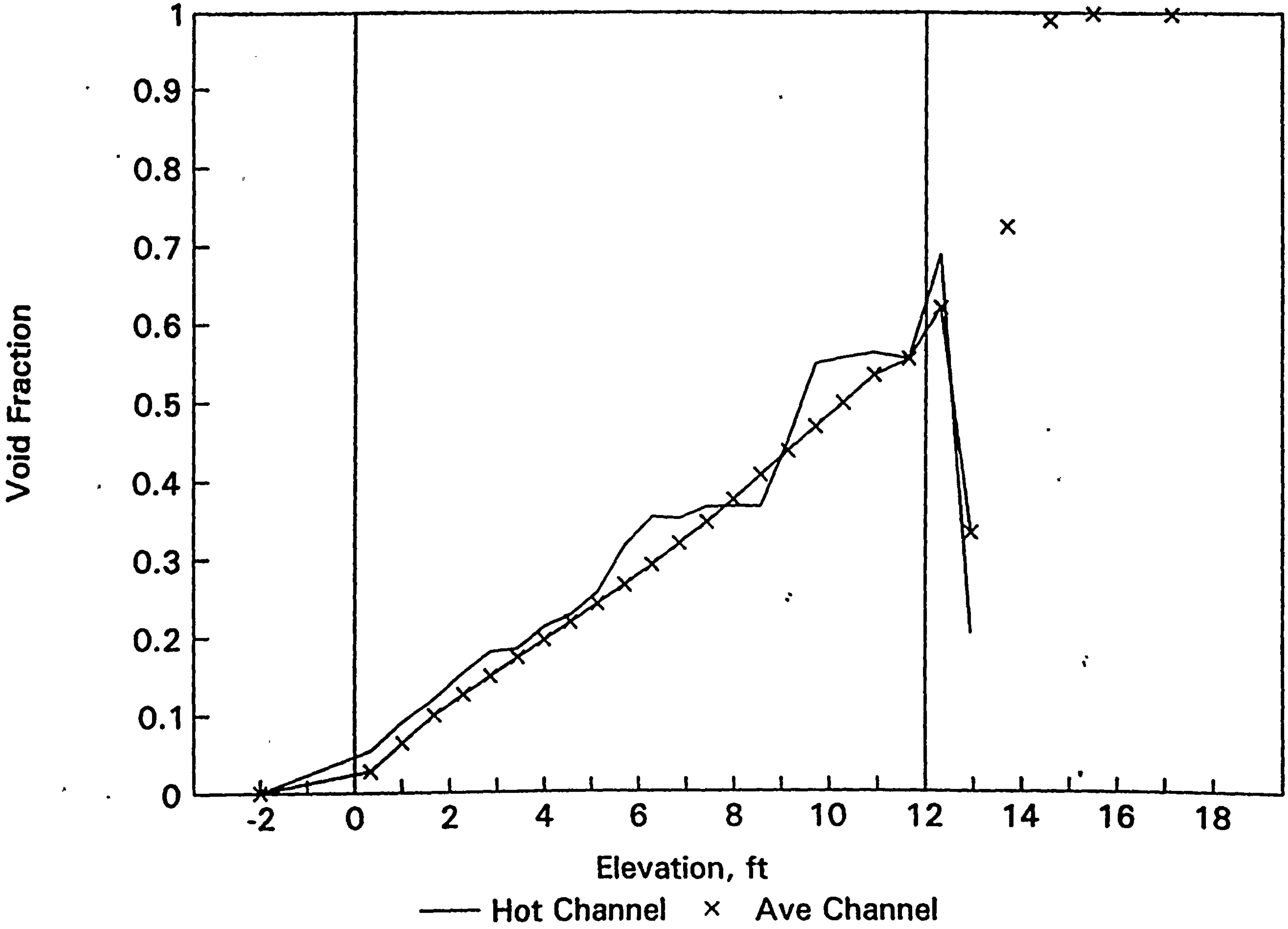
At 700 seconds, the two upper volumes of the heated core are showing substantial voiding and the very top heated node may be experiencing some heatup. For the limited uncovery apparent here, mist entrainment from the mixture may be sufficient to prevent core heatup. The hot and average mixture levels are in agreement as the uncovery proceeds. At 710 seconds, the mixture has fallen to its lowest level during loop seal clearing. The hot and average channel mixture levels remain in agreement with the hot channel slightly more voided. At 715 seconds, the loop seal for the broken loop has cleared and the downcomer and core levels are starting to equilibrate creating a core refill. By 720 seconds, the refill has progressed into the upper plenum. The void fraction at the very outlet of the hot channel is again depressed but that was not observed in the partial refill at 715 seconds. Thus, the predictions of the hot channel exit void fraction are consistent with the needs of the transient prediction, attaining the required degree of accuracy under conditions when core uncovery is occurring or eminent. By 800 seconds, the refill is complete and the core boil down phase has been entered. As shown, the refill did not completely fill the vessel. The region just below the out nozzle remains at an elevated void content and the upper plenum at the outlet nozzle elevation is completely voided.

In conclusion, core modeling has been arranged to provide for hot and average channel effects. Specific provisions have been incorporated into the EM to achieve conservative predictions of cladding temperature (crossflow resistance for the upper half of the core). The modeling works well during core uncovery as evidenced by the agreement between the hot and average channel mixture levels. Although a modeling factor does lead to an apparently inconsistent void fraction in an upper unheated volume of the hot channel during those phases of the SBLOCA transient when the upper plenum contains mixture, this difficulty is resolved as the core uncovers and is not present at any time that the calculation is predicting core uncovery or calculating cladding temperature excursions. Therefore, the core modeling approach employed is appropriate for the calculation of small break LOCA simulations.


CORE VOID DISTRIBUTION - 3 in Break
640 seconds
Void Fraction
0
0.1
0.2
0.3
0.4
0.5
0.6
0.7
0.8
0.9
1
-2
0
2
4
6
8
10
12
14
16
18
Elevation, ft
Hot Channel
Ave Channel

CORE VOID DISTRIBUTION - 3 in Break

660 seconds



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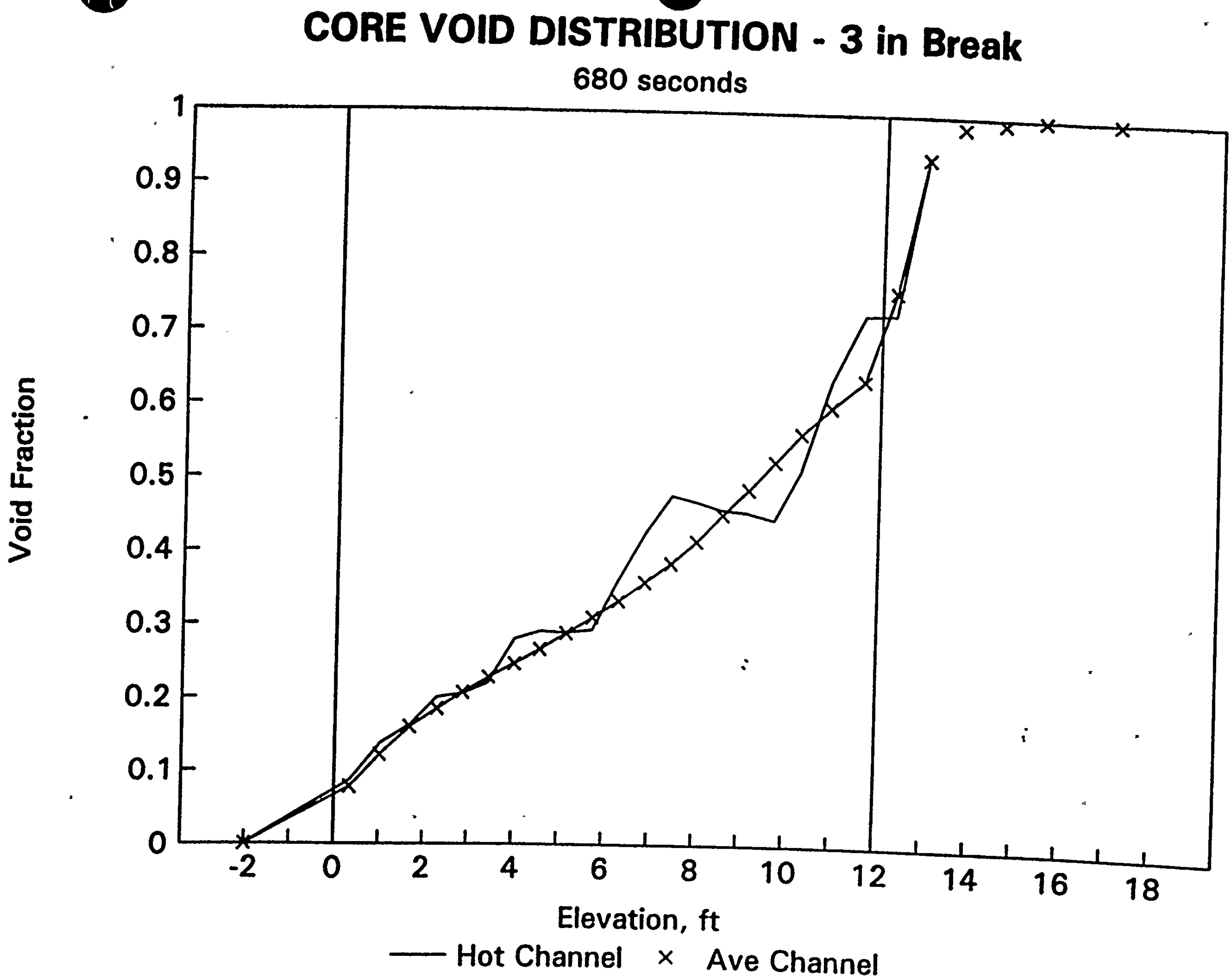
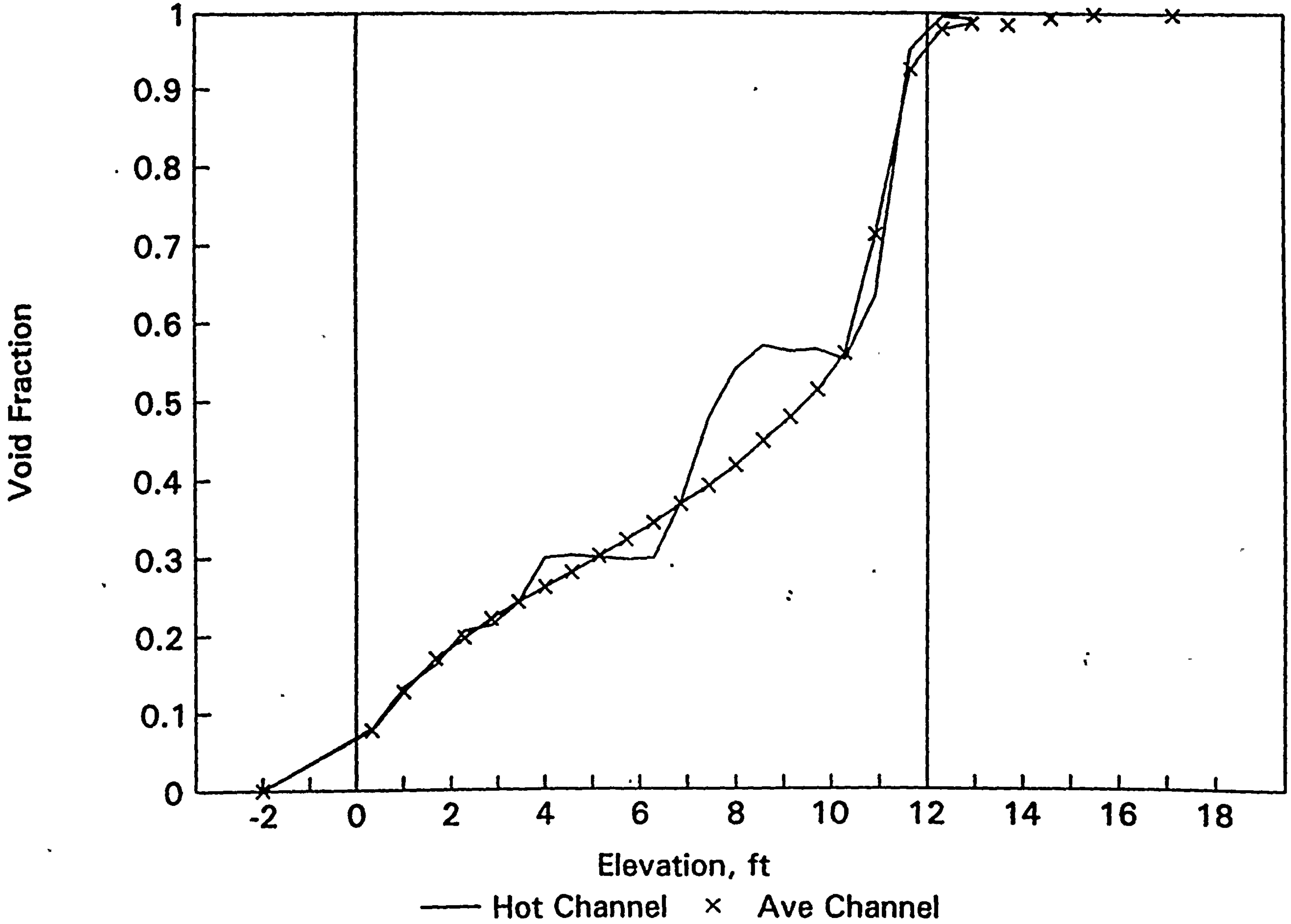

CORE VOID DISTRIBUTION - 3 in Break
680 seconds
Void Fraction
1
0.9
0.8
0.7
0.6
0.5
0.4
0.3
0.2
0.1
0
-2
0
2
4
6
8
10
12
14
16
18
Elevation, ft
Hot Channel
Ave Channel

38

39

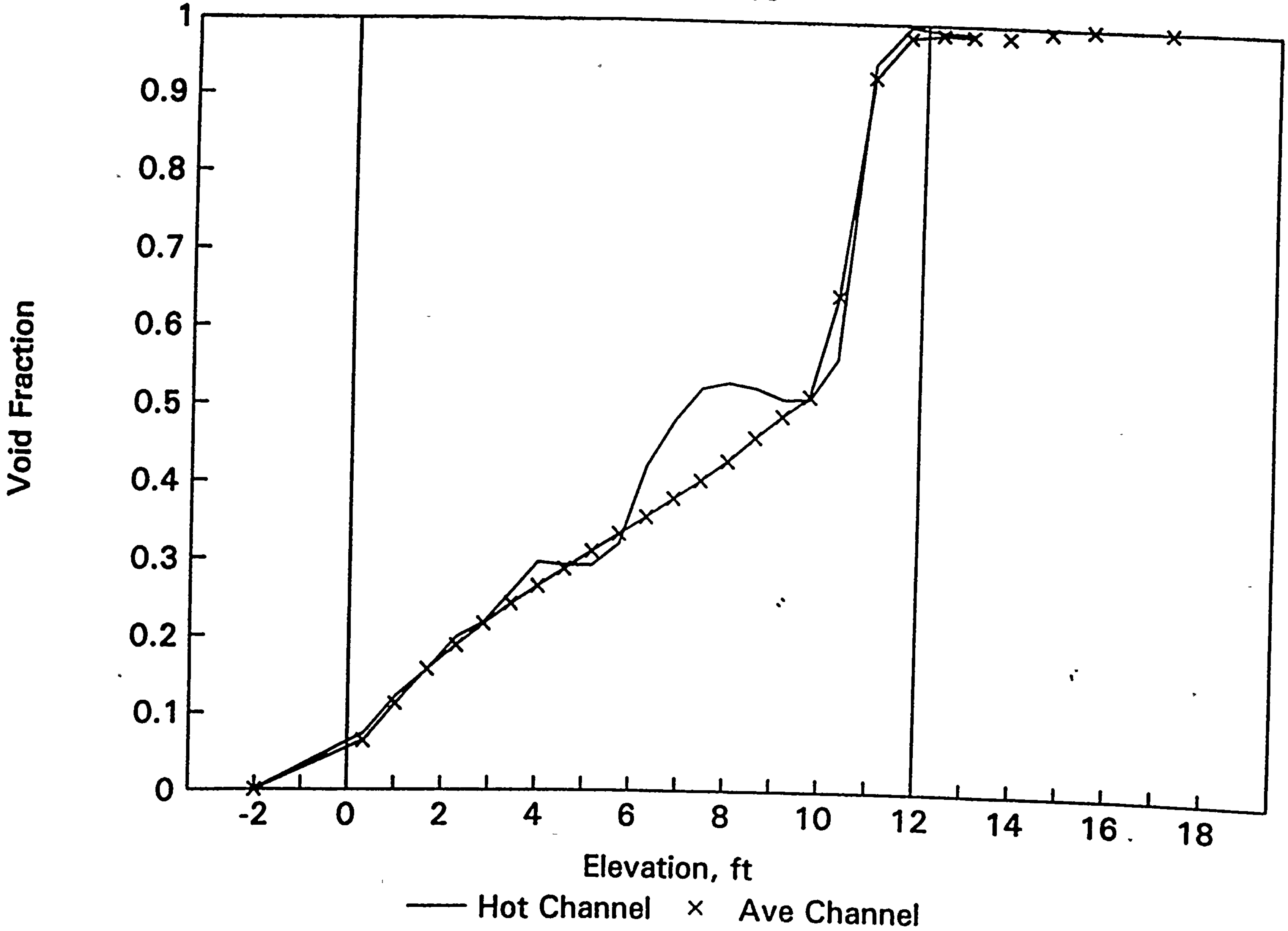
CORE VOID DISTRIBUTION - 3 in Break

690 seconds



CORE VOID DISTRIBUTION - 3 in Break

700 seconds



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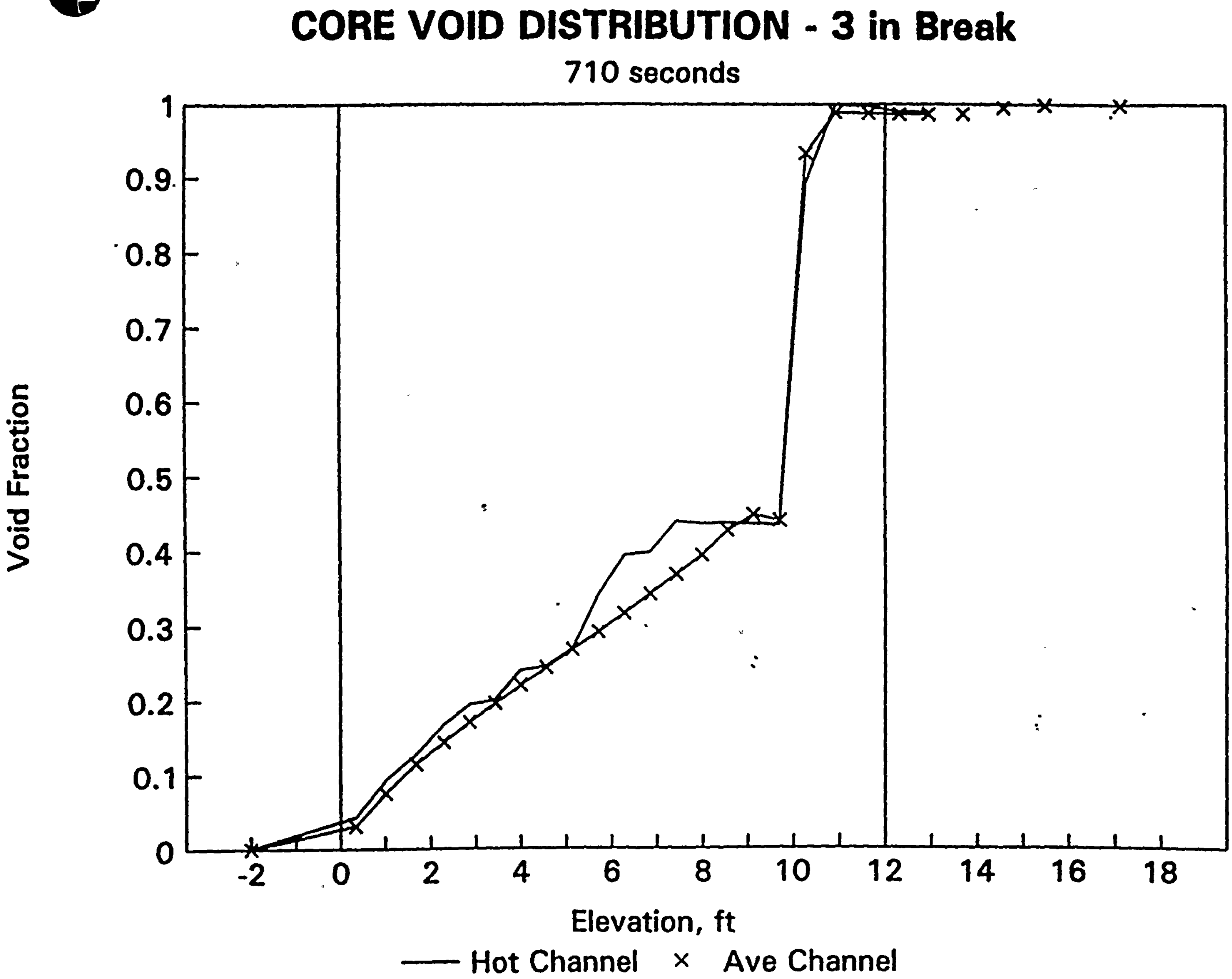
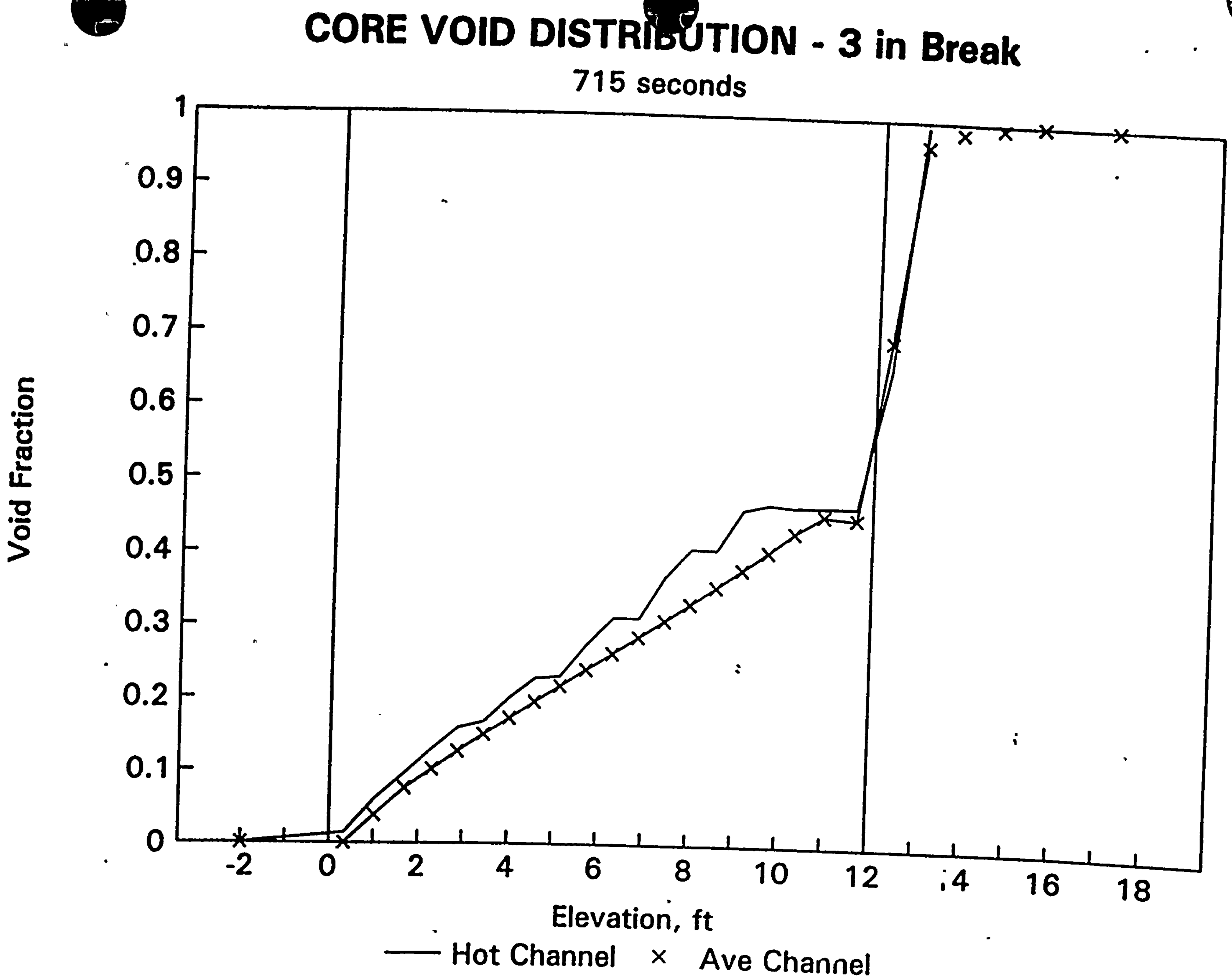

CORE VOID DISTRIBUTION - 3 in Break
710 seconds
Void Fraction
1
0.9
0.8
0.7
0.6
0.5
0.4
0.3
0.2
0.1
0
-2
0
2
4
6
8
10
12
14
16
18
Elevation, ft
Hot Channel
Ave Channel

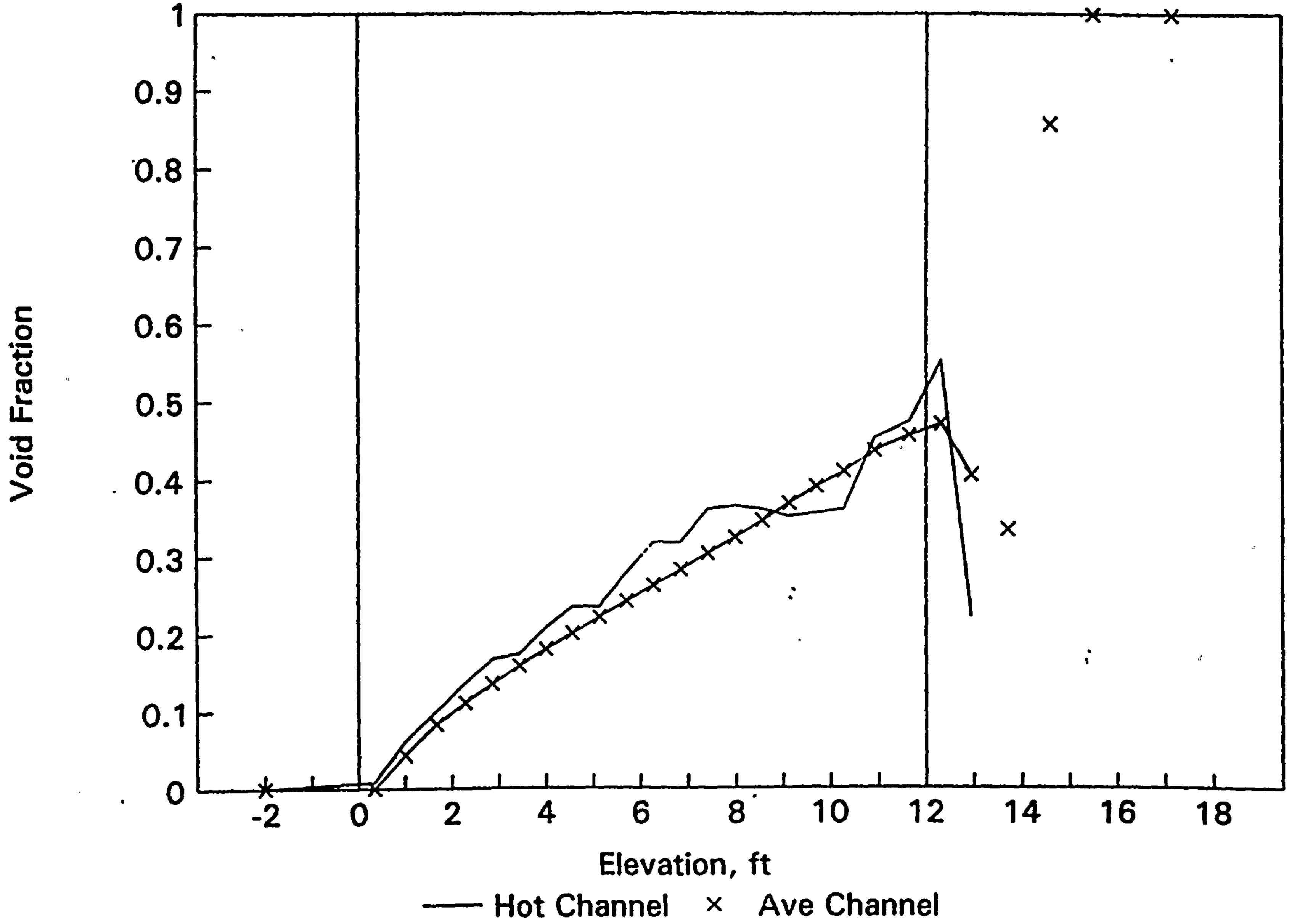
41


CORE VOID DISTRIBUTION - 3 in Break
715 seconds
Void Fraction
0
0.1
0.2
0.3
0.4
0.5
0.6
0.7
0.8
0.9
1
-2
0
2
4
6
8
10
12
14
16
18
Elevation, ft
Hot Channel
Ave Channel

42

CORE VOID DISTRIBUTION - 3 in Break

720 seconds



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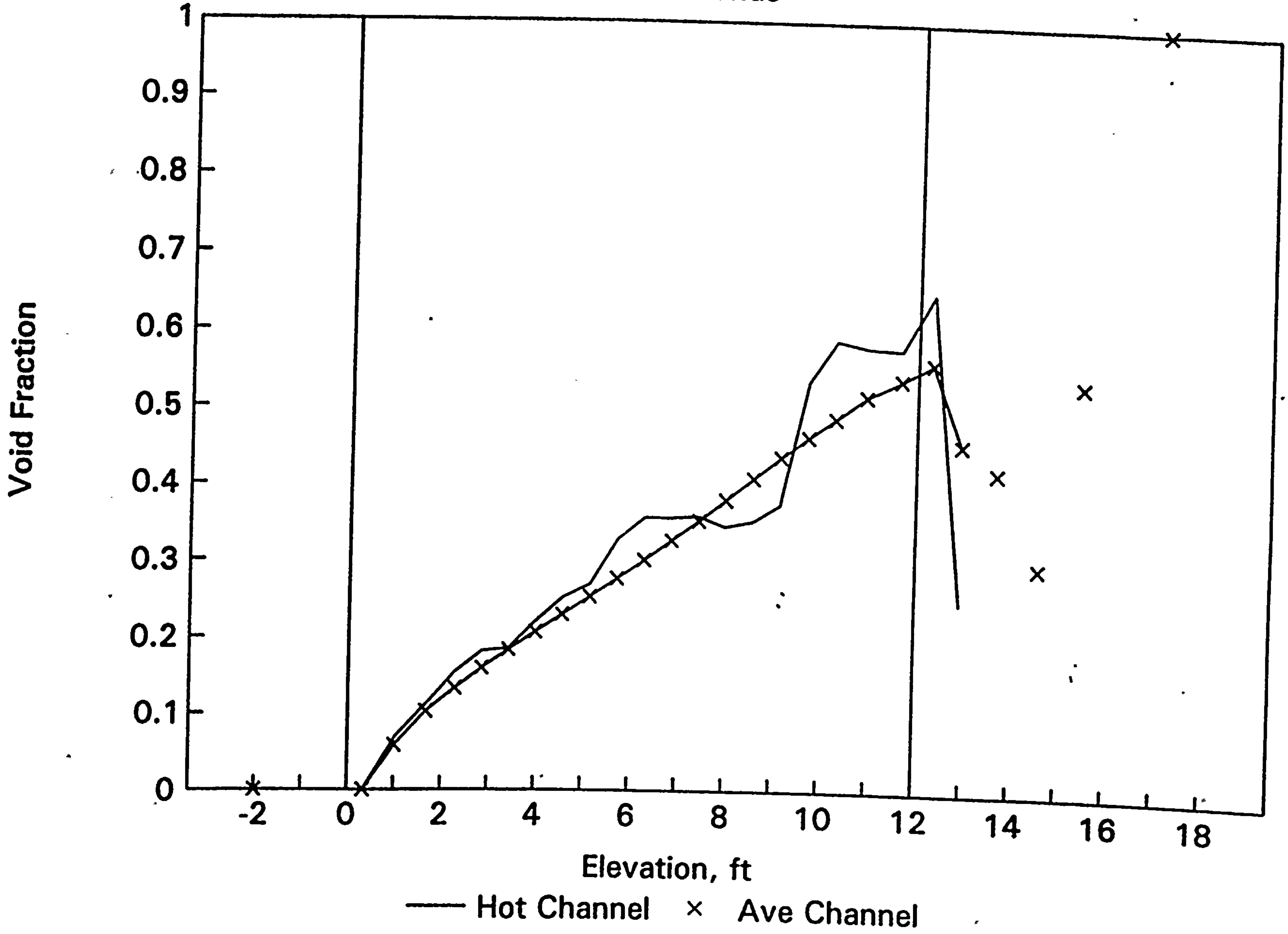
CORE VOID DISTRIBUTION - 3 in Break

800 seconds



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Supplementary Break Orientation Information:

Range of Upper Head Spray Nozzle Areas:

T-hot Plant	=>	≈ 0.02 ft^2	(Trojan, North Anna, Surry, etc.)
T-cold Plant	=>	≈ 0.45 ft^2	(McGuire/Catawba, Sequoia, etc.)

Some plants sit in between these limits with areas of 0.2 or 0.3 ft^2.

The inclosed plots are for the 2.1 inch case that was provided in an earlier communication. I felt that with them being part of a larger set they would be more useful. If the specific 2 inch case is important we can reconstruct it and send the same plots. Some of the definitions on the plots are:

UP	Upper Plenum
V	Volume or Node
AC	Average Channel
HC	Hot Channel
CVAR	Control Variable For the case of AC CVAR and HC CVAR the display is a collapsed water level for the core region with 0.0 taken at the bottom of the active region. The reason that the values exceed 12 feet is the inclusion to the two unheated volumes of the fuel assemblies that model the fuel pins above the uranium pellets and the upper nozzle of the fuel assembly.
Jun	Junction or Flow Path
J	Junction or Flow Path
UH SPRAY	Upper Head Spray Nozzle
IL ECC	Intact loops ECCS flow
BL ECC	Broken Loop ECCS flow For this case IL ECC CVAR and BL ECC CVAR are simply the high pressure injections. Had the plant depressurized these control variables would have picked up the accumulators and the low head systems.
HOT CH	Hot channel, HOT CH, CVAR is a control variable that approximates the mixture level in the core hot channel. For the purpose of this CVAR mixture is defined as $\alpha < 0.9$. The control variable samples the α from the bottom to the top in each node of the channel. If α is less than 0.9 the height of the volume is considered mixture once α is greater than 0.9 the control volume is considered as above the mixture and the search stopped.

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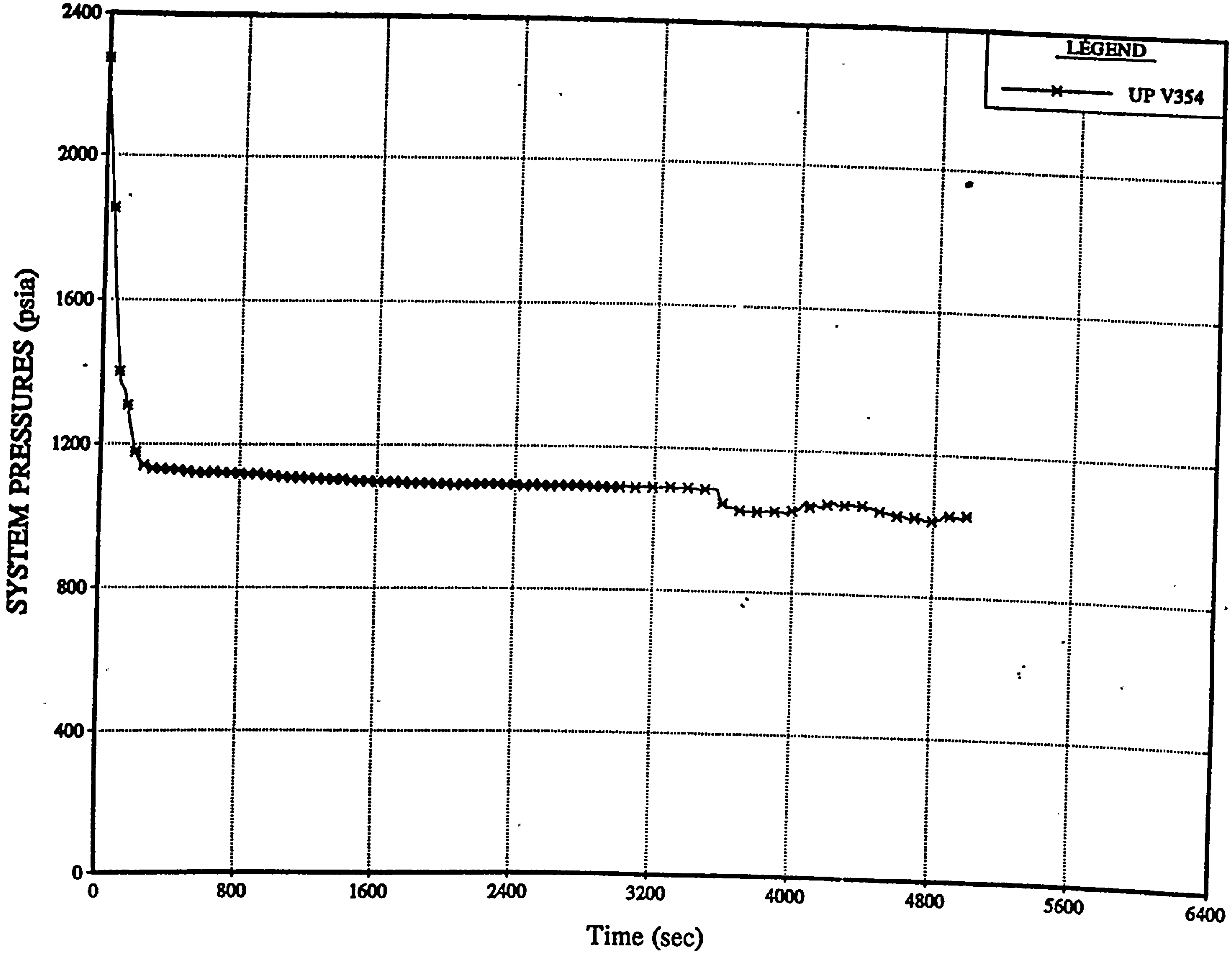
RELAP5/MOD2 Ver 20.0HP



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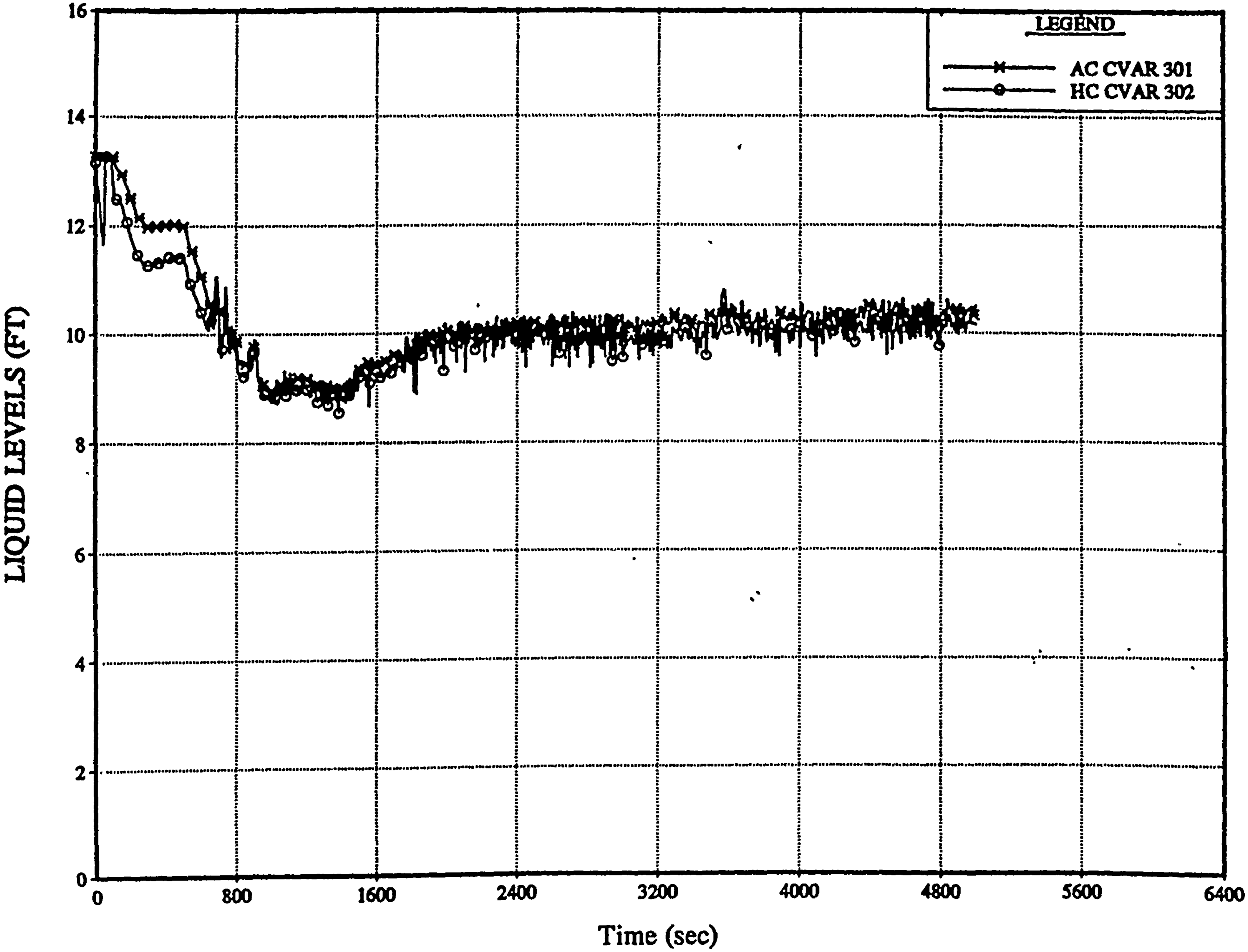
R5/2 2.1 INCH PD BREAK Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP



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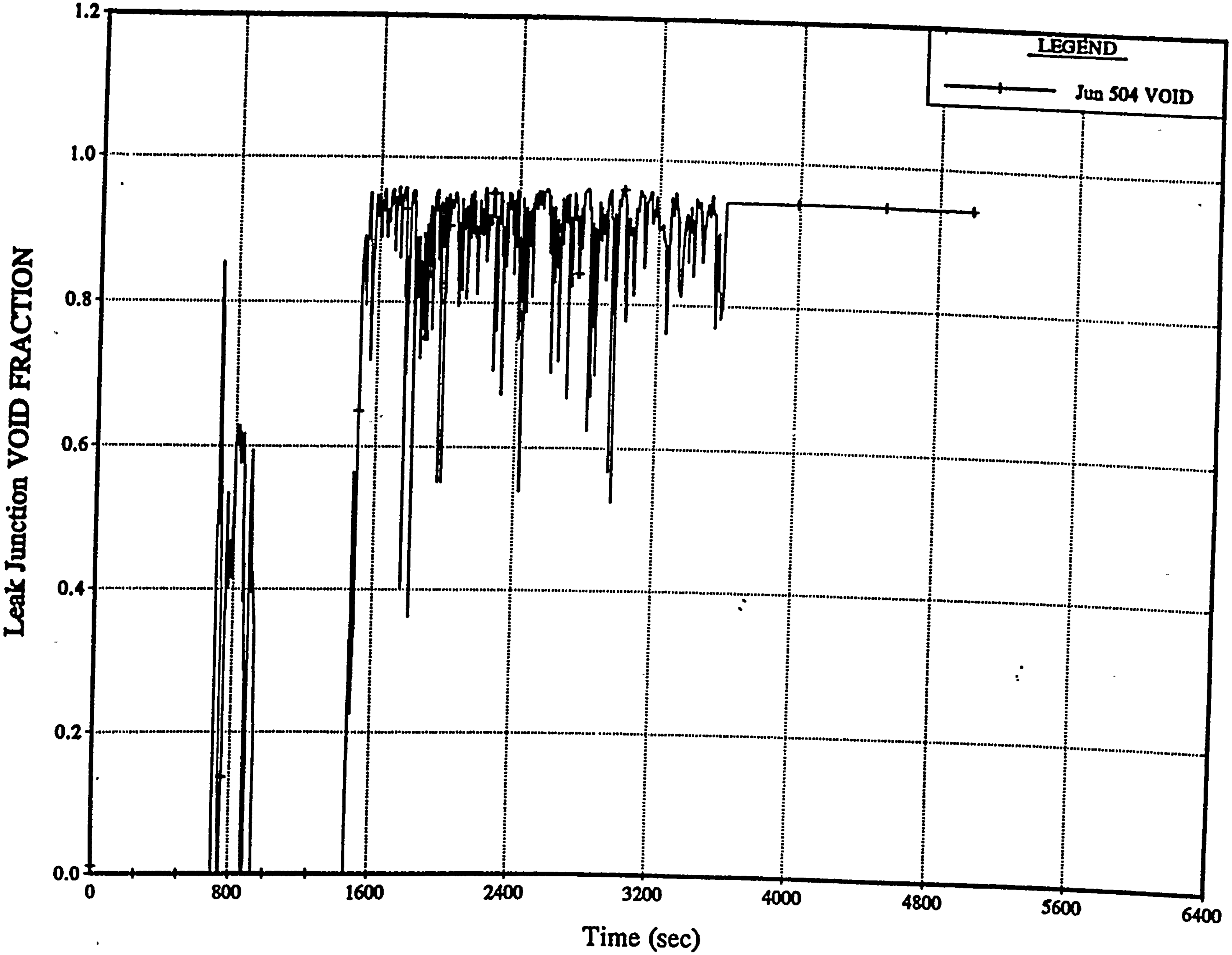
R5/2 2.1 INCH PD BREAK Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP



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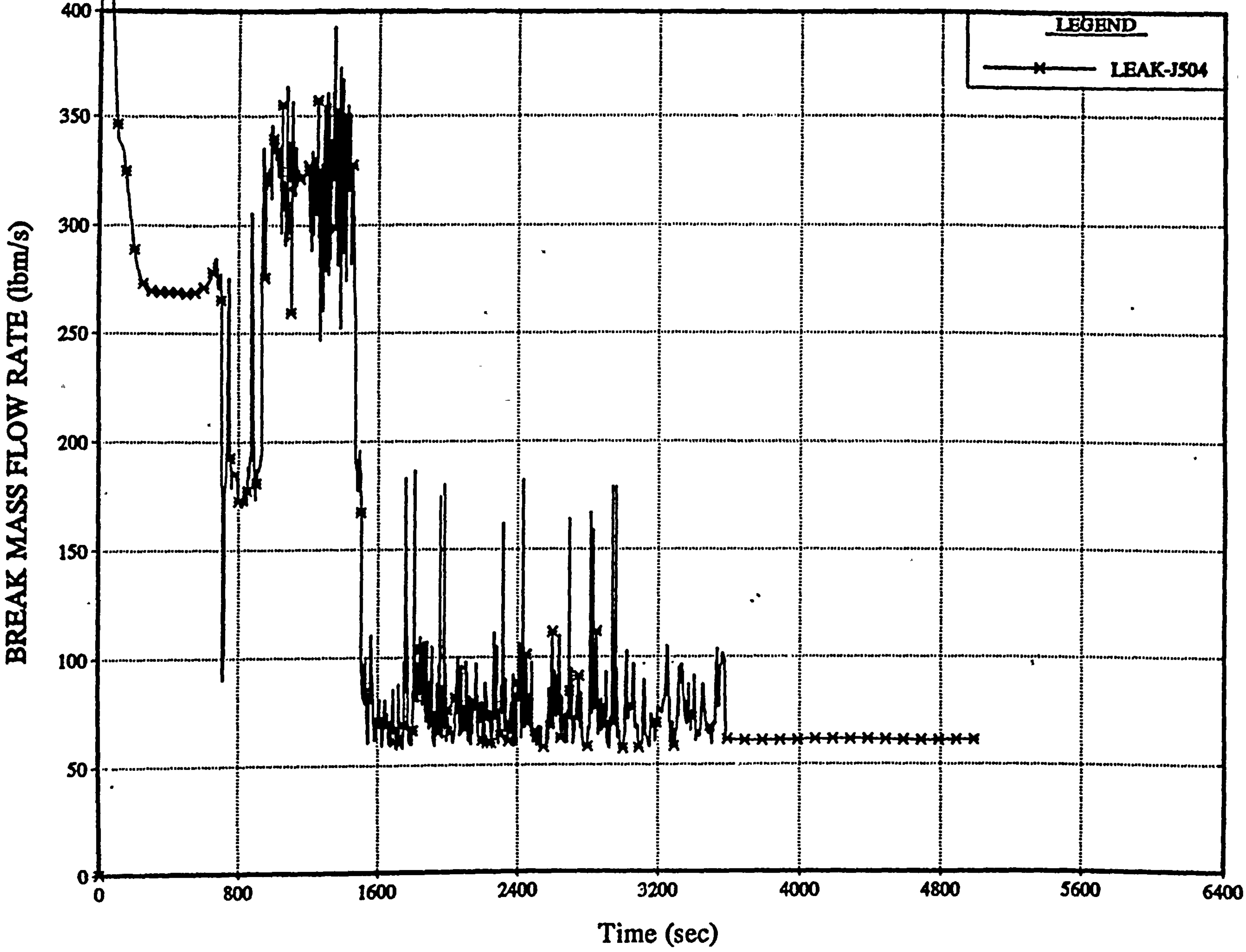
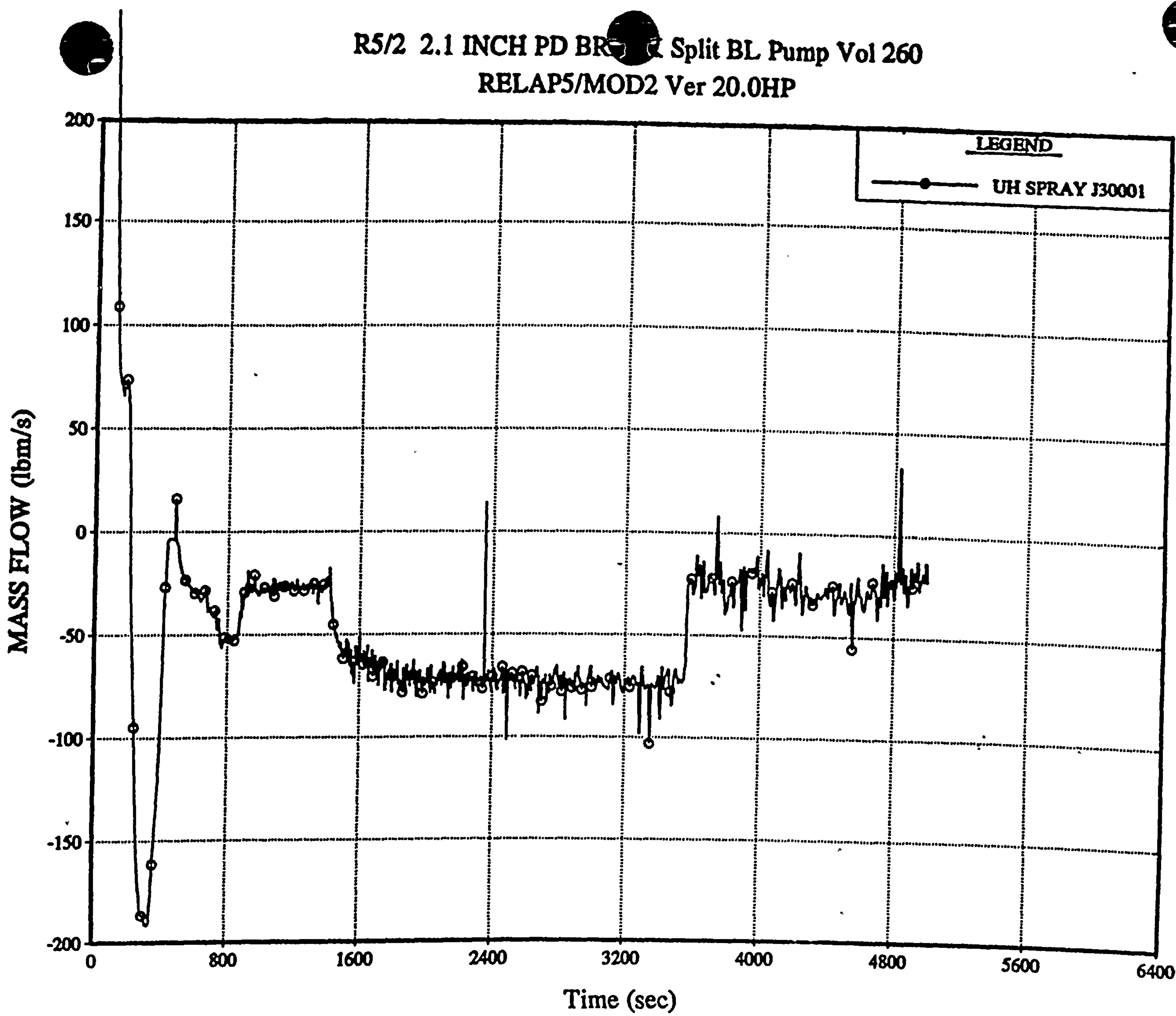
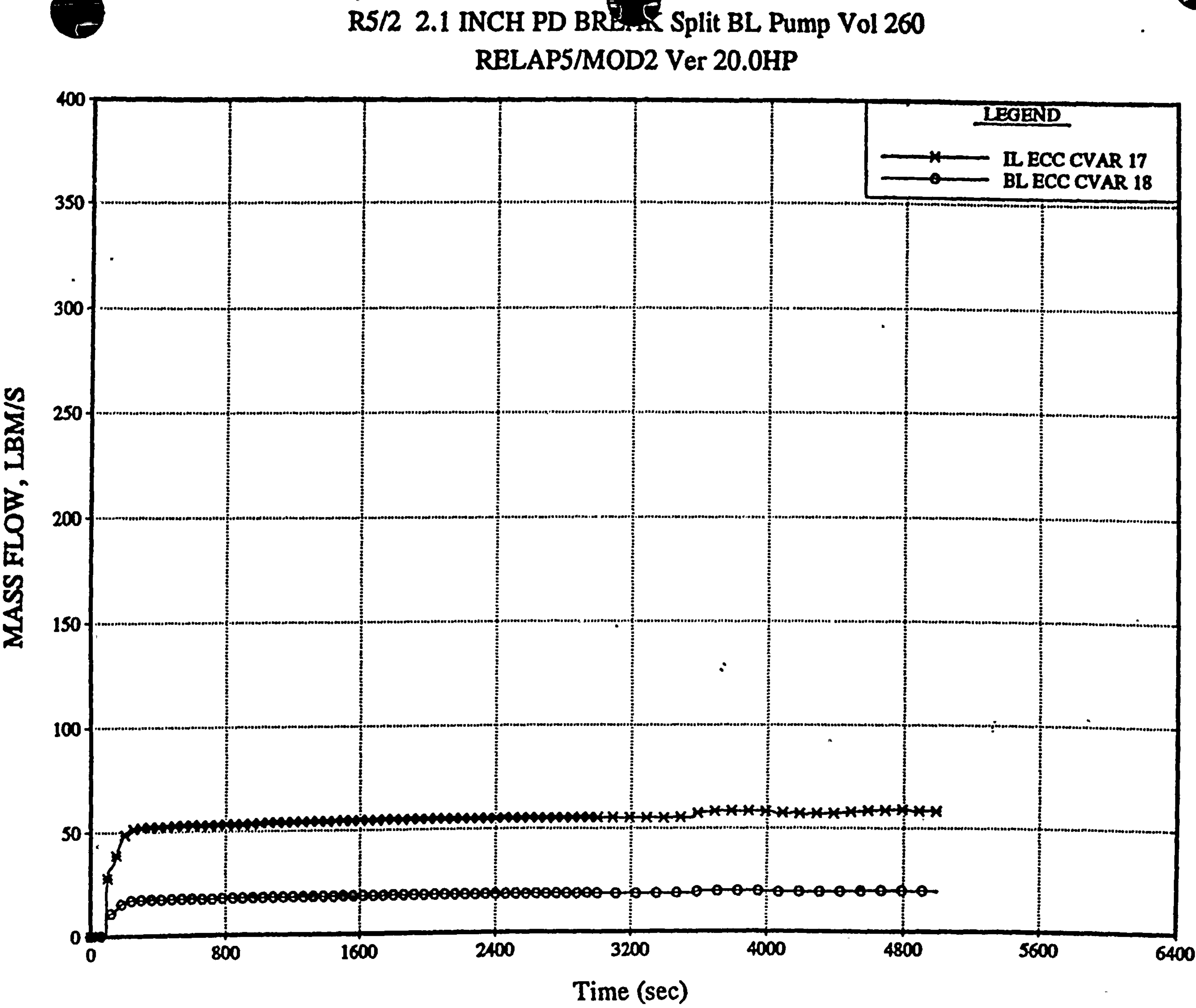
R5/2 2.1 INCH PD BREAK Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP





TATB VWRO MFLOW015



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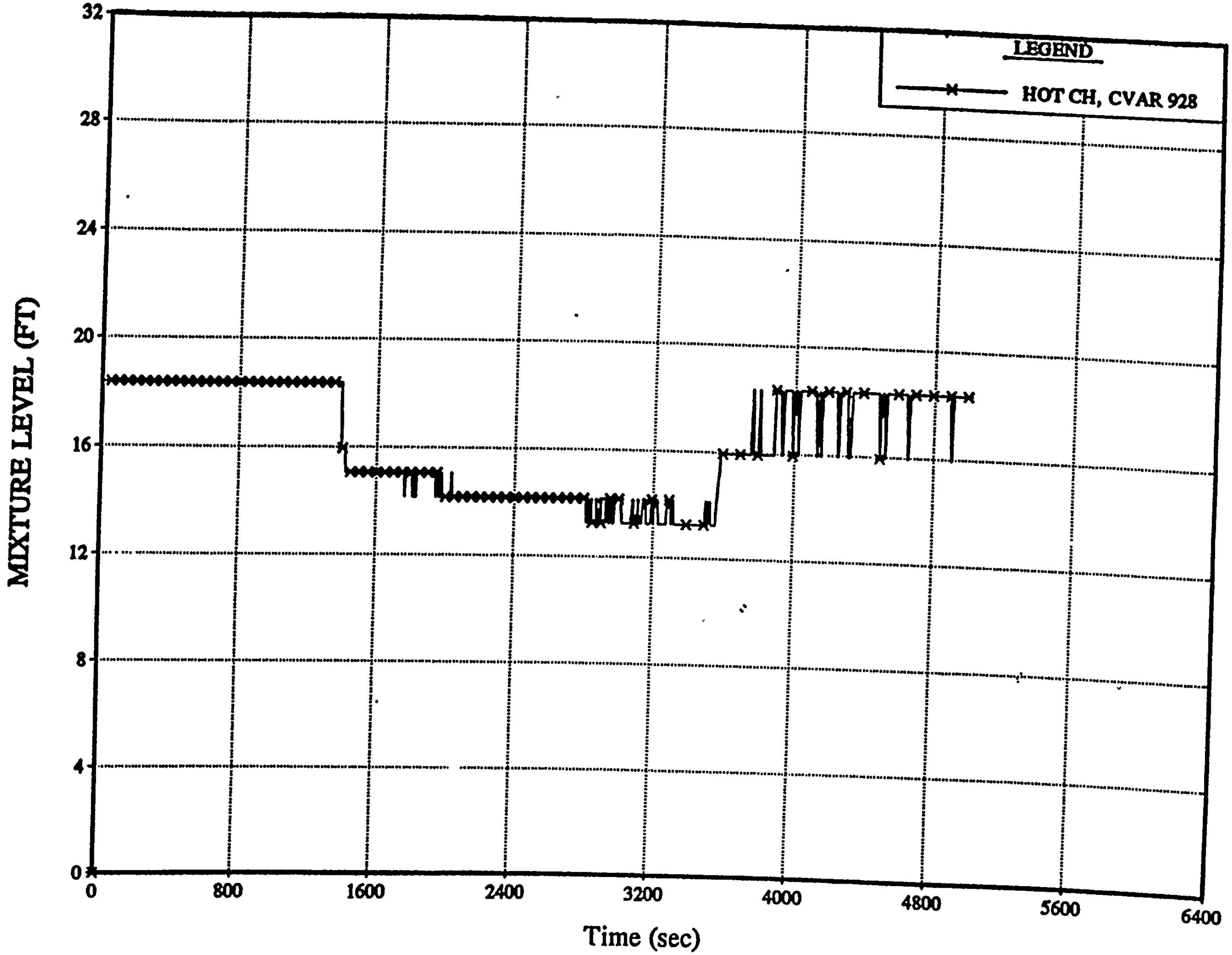
TATR YWRO CNTRL048

R5/2 2.1 INCH PD BR[illegible] Split BL Pump Vol 260
RELAP5/MOD2 Ver 20.0HP



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SBLOCA Long-Term Cooling: In our 3/28/96 telecon, Bob Jones raised an issue as to the sufficiency of FTI's SBLOCA long-term cooling write-up on page 8-1 of BAW-10168, Revision 2, Volume II. He indicated that the appropriateness of the methodology was difficult to judge relative to the criterion of 10CFR50.46. As stated on page 8-1, FTI's SBLOCA long-term cooling methodology is basically the same as that used for LBLOCA and discussed in detail on page 8-1 of Volume I. It is repeated below.

FTI continues its transient small break LOCA computer analysis until the core is covered by mixture and the clad temperatures have decreased to the coolant saturation temperature. For the long-term, the clad will be maintained within several degrees of the coolant saturation temperature by a continuous flow of ECC water. Each plant has established NRC-approved procedures for an orderly transition to long-term cooling, assuring a continuous flow of ECC water to the reactor vessel and preventing the crystallization of boric acid in the core. The plant procedures specify the operator actions necessary to switch to sump recirculation--providing for a continuous ECC flow--and to assure a throughput of water to the core--maintaining boric acid concentrations at or below previously-established acceptable levels.

FTI plant applications performed under BAW-10168 will validate the appropriateness of previously-established operator action times, assuring the effective establishment of long-term cooling. If the need for new operator action times is demonstrated, analyses necessary to do so will be performed for and reported in the plant-specific LOCA application. For SBLOCA, such calculations are usually unnecessary, since, in general, it is bounded by LBLOCA predictions and that analysis is used to satisfy the long-term cooling criterion. In FTI's approach, the LOCA/plant procedure interface is properly addressed and in combination with as-designed plant emergency systems requirements the long-term cooling criterion of 10CFR50.46 is satisfied.

Equilibrium Core Heat Transfer Calculations: FTI's original NRC-approved evaluation model (for both large and small breaks)--BAW-10168, Revision 1--used equilibrium conditions for the RELAP5 computation of core heat transfer; this issue was thoroughly explored by the INEL reviewers and it was approved by the NRC. In Revision 2 of the EM, FRAP-T6 was deleted from the large break LOCA calculational technique. No changes were made to the core heat transfer package other than the calculations for the hot channel were now performed in RELAP5. The modeling was still based on equilibrium and it was found to be acceptable for licensing use by the NRC. In Revision 3, FRAP-T6 was deleted from SBLOCA. Again, no changes, other than code location, were made to the equilibrium core heat transfer package.

When the RSG evaluation model was originally assembled, FTI installed in RELAP5 core heat transfer correlations, covering most of the boiling curve, that were formulated based on equilibrium conditions. The RELAP5 core heat transfer package, designed after that in FRAP-T6, was used and approved for both large and small break applications. The EM was benchmarked, most recently against ROSA IV, and shown to produce conservative PCTs. FTI understands that it could upgrade RELAP5 to a nonequilibrium core heat transfer calculation, but it would require a substantial investment (code revisions, benchmarks, topical report revisions, and licensing) and there is no identified calculational or safety benefit to such a modification. Therefore, FTI has

decided to continue to use the equilibrium option. The T-H role of RELAP5 is unchanged, and an equilibrium core heat transfer calculation, previously found acceptable in FRAP-T6, is still being used and has already been approved for LBLOCA calculations. The RELAP5 equilibrium approach is NRC-approved and the removal of FRAP-T6 from the SBLOCA EM has no bearing on its continued validity.