WCAP-16640-NP, August 2006, Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet at Salem Unit 1

Westinghouse Non-Proprietary Class 3

Westinghouse Non-Proprietary Class 3

WCAP-16640-NP

August 2006

Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet at Salem Unit 1

Westinghouse

WCAP-16640-NP

Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet at Salem Unit 1

Gary Whiteman
Regulatory Compliance and Plant Licensing

August 2006

Reviewer: *_____
C. D. Cassino
Steam Generator Design and Analysis

Approved: *_____
Earl Morgan, Manager
Chemistry Diagnostics & Materials Engineering

Approved: *_____
Gordon Bischoff, Programs Manager
PWR Owners Group

This work was performed under PWROG Project Number PA-MSC-0272

***Electronically Approved Records Are Authenticated in the Electronic Document Management System**

Westinghouse Electric Company LLC
P.O. Box 355
Pittsburgh, PA 15230-0355

© 2006 Westinghouse Electric Company LLC
All Rights Reserved

LEGAL NOTICE

This report was prepared as an account of work performed by Westinghouse Electric Company LLC. Neither Westinghouse Electric Company LLC, nor any person acting on its behalf:

A. Makes any warranty or representation, express or implied including the warranties of fitness for a particular purpose or merchantability, with respect to the accuracy, completeness, or usefulness of the information contained in this report, or that the use of any information, apparatus, method, or process disclosed in this report may not infringe privately owned rights; or

B. Assumes any liabilities with respect to the use of, or for damages resulting from the use of, any information, apparatus, method, or process disclosed in this report.

COPYRIGHT NOTICE

This report has been prepared by Westinghouse Electric Company LLC and bears a Westinghouse Electric Company copyright notice. As a member of the Pressurized Water Reactor Owners Group, you are permitted to copy and redistribute all or portions of the report within your organization; however all copies made by you must include the copyright notice in all instances.

DISTRIBUTION NOTICE

This report was prepared for the PWR Owners Group. This Distribution Notice is intended to establish guidance for access to this information. This report (including proprietary and non-proprietary versions) is not to be provided to any individual or organization outside of the PWR Owners Group program participants without prior written approval of the PWR Owners Group Program Management Office. However, prior written approval is not required for program participants to provide copies of Class 3 Non-Proprietary reports to third parties that are supporting implementation at their plant, and for submittals to the NRC.

PWR Owners Group
Member Participation* for PWROG Project PA-MSC-0272

Utility Member	Plant Site(s)	Participant	
		Yes	No
AmerenUE	Callaway (W)		X
American Electric Power	D.C. Cook 1& 2 (W)		X
Arizona Public Service	Palo Verde Unit 1, 2, & 3 (CE)		X
Constellation Energy Group	Calvert Cliffs 1 & 2 (CE)		X
Constellation Energy Group	Ginna (W)		X
Dominion Connecticut	Millstone 2 (CE)		X
Dominion Connecticut	Millstone 3 (W)	X	
Dominion Kewaunee	Kewaunee (W)		X
Dominion VA	North Anna Unit 1 & 2 (W), Surry 1 & 2 (W)		X
Duke Energy	Catawba 1 & 2, McGuire 1 & 2 (W), Oconee 1, 2 & 3 (B&W)		X
Entergy Nuclear Northeast	Indian Point 2 & 3 (W)		X
Entergy Operations South	Arkansas 2, Waterford 3 (CE), Arkansas 1 (B&W)		X
Exelon Generation Co. LLC	Braidwood 1 & 2, Byron 1 & 2 (W), TMI 1 (B&W)		X
FirstEnergy Nuclear Operating Co	Beaver Valley 1 & 2 (W), Davis-Besse (B&W)		X
Florida Power & Light Group	St. Lucie 1 & 2 (CE)		X
Florida Power & Light Group	Turkey Point 3 & 4 , Seabrook (W)		X
Nuclear Management Company	Prairie Island 1 & 2, Point Beach 2 (W)		X
Nuclear Management Company	Point Beach 1 (W)	X	
Nuclear Management Company	Palisades (CE)		X
Omaha Public Power District	Fort Calhoun (CE)		X
Pacific Gas & Electric	Diablo Canyon 1 & 2 (W)		X
Progress Energy	H.B. Robinson (W)	X	
Progress Energy	Shearon Harris (W), Crystal River 3 (B&W)		X
PSEG – Nuclear	Salem 1 (W)	X	
PSEG - Nuclear	Salem 2 (W)		X
Southern California Edison	SONGS 2 & 3 (CE)		X
South Carolina Electric & Gas	V.C. Summer (W)		X
South Texas Project Nuclear Operating Co.	South Texas Project 1 & 2 (W)		X
Southern Nuclear Operating Co.	Farley 1 & 2, Vogtle 1 & 2 (W)		X
Tennessee Valley Authority	Sequoyah 1 & 2, Watts Bar (W)		X
TXU Power	Comanche Peak 1 (W)		X
TXU Power	Comanche Peak 2 (W)	X	
Wolf Creek Nuclear Operating Co.	Wolf Creek (W)		X

* Project participants as of the date the final deliverable was completed. On occasion, additional members will join a project. Please contact the PWROG Program Management Office to verify participation before sending this document to participants not listed above.

PWR Owners Group
International Member Participation* for PWROG Project PA-MSC-0272

Utility Member	Plant Site(s)	Participant	
		Yes	No
British Energy	Sizewell B		X
Electrabel (Belgian Utilities)	Doel 1, 2 & 4, Tihange 1 & 3		X
Kansai Electric Co., LTD	Mihama 1, Ohi 1 & 2, Takahama 1 (W)		X
Korea Hydro & Nuclear Power Corp.	Kori 3 & 4 Yonggwang 2 (W)		X
Korea Hydro & Nuclear Power Corp.	Kori 1, 2 Yonggwang 1 (W)	X	
Korea Hydro & Nuclear Power Corp.	Yonggwang 3, 4, 5 & 6 Ulchin 3, 4, 5 & 6 (CE)		X
Nuklearna Electrarna KRSKO	Krsko (W)		X
Nordostschweizerische Kraftwerke AG (NOK)	Beznau 1 & 2 (W)		X
Ringhals AB	Ringhals 2, 3 & 4 (W)		X
Spanish Utilities	Asco 1 & 2, Vandellos 2, Almaraz 1 & 2 (W)		X
Taiwan Power Co.	Maanshan 1 & 2 (W)		X
Electricite de France	54 Units		X

* This is a list of participants in this project as of the date the final deliverable was completed. On occasion, additional members will join a project. Please contact the PWROG Program Management Office to verify participation before sending documents to participants not listed above.

TABLE OF CONTENTS

LIST OF TABLES vii

LIST OF FIGURES ix

1.0 EXECUTIVE SUMMARY 1-1

2.0 BACKGROUND 2-1

3.0 INTRODUCTION 3-1

4.0 SUMMARY DISCUSSION 4-1

5.0 OPERATING CONDITIONS 5-1

5.1 BOUNDING OPERATING CONDITIONS 5-1

5.2 FAULTED CONDITIONS 5-1

6.0 STEAM GENERATOR TUBE PULLOUT AND LEAKAGE TEST PROGRAM DISCUSSION 6-1

6.1 TUBE PULLOUT RESISTANCE PROGRAMS 6-2

6.2 LEAK RATE TESTING PROGRAMS 6-3

6.3 LOSS COEFFICIENT ON CONTACT PRESSURE REGRESSION 6-9

7.0 STRUCTURAL ANALYSIS OF TUBE-TO-TUBESHEET JOINT 7-1

7.1 EVALUATION OF TUBESHEET DEFLECTION EFFECTS FOR TUBE-TO-TUBESHEET CONTACT PRESSURE 7-2

7.2 DETERMINATION OF REQUIRED ENGAGEMENT LENGTH OF THE TUBE IN THE TUBESHEET 7-10

7.3 NDE UNCERTAINTY DISCUSSION 7-12

7.4 DIVIDER PLATE CRACKING ISSUE 7-13

8.0 LEAK RATE ANALYSIS OF CRACKED TUBE JOINTS 8-1

8.1 THE BELLWETHER PRINCIPLE FOR NORMAL OPERATION TO STEAM LINE BREAK LEAK RATES 8-1

8.2 LIGAMENT TEARING DISCUSSION 8-4

9.0 DETERMINATION OF THE B* DISTANCE 9-1

9.1 BACKGROUND INFORMATION 9-2

9.2 FLOW THROUGH A CREVICE (DARCY'S EQUATION) 9-3

9.3 TUBE-TO-TUBESHEET CONTACT PRESSURE VARIATION 9-4

9.4 DETERMINATION OF THE B* DISTANCE 9-5

9.5 SENSITIVITY OF THE B* CALCULATION 9-8

9.6 ADDITIONAL LEAKAGE SENSITIVITY STUDY FOR 8 INCH INSPECTION LENGTH CRITERIA COMPLETED FOR 11/16 OD TUBING 9-8

9.7 CONCLUSIONS RELATIVE TO B* 9-13

10.0 NRC STAFF DISCUSSION FOR ONE CYCLE APPROVAL B* BRAIDWOOD UNIT 2 10-1

10.1 JOINT STRUCTURAL INTEGRITY DISCUSSION 10-1

10.2 JOINT LEAKAGE INTEGRITY DISCUSSION 10-8

11.0 CONCLUSIONS 11-1

EXAMPLE APPLICATION FOR SALEM UNIT 1 11-3

12.0 REFERENCES 12-1

LIST OF TABLES

Table 6-1. Model F Leak Test Program Matrix 6-10

Table 6-2. Model D5 Leak Test Program Matrix 6-11

Table 6-3. Model F Leak Rate Testing Data 6-12

Table 6-4. Model D5 Experimental Loss Coefficients 6-13

Table 6-5. Model F 0.25 Inch Displacement Pullout Test Data 6-15

Table 6-6. Model F Small Displacement Data at 600°F 6-15

Table 6-7. Model D5 0.25 Inch Displacement Pullout Test Data 6-16

Table 6-8. Model 44F 0.25 Inch Displacement Pullout Test Data 6-17

Table 7-1. Summary of Material Properties Alloy 600 Tube Material 7-16

Table 7-2. Summary of Material Properties for SA-508 Class 2a Tubesheet Material 7-16

Table 7-3. Summary of Material Properties SA-533 Grade A Class 2 Shell Material 7-16

Table 7-4. Summary of Material Properties SA-216 Grade WCC Channelhead Material 7-17

Table 7-5. Equivalent Solid Plate Elasticity Coefficients for 44F Perforated TS SA-508 Class 2a Tubesheet Material (psi) 7-17

Table 7-6. Tube/Tubesheet Maximum & Minimum Contact Pressures and H* Depths for Salem Unit 1 Steam Generators (Hot Leg and Cold Leg) 7-18

Table 7-7. Cumulative Forces Resisting Pull Out from the TTS Salem Unit 1 Hot Leg Normal Conditions – T_{hot} = 601.8°F P_{sec} = 731 psig 7-19

Table 7-8. Cumulative Forces Resisting Pull Out from the TTS Salem Unit 1 Hot Leg Normal Conditions – T_{hot} = 613.1°F, P_{sec} = 807 psig 7-20

Table 7-9. Cumulative Forces Resisting Pull Out from the TTS Salem Unit 1 Faulted (SLB) Conditions, Psec = 0 psig (Hot and Cold Leg) 7-21

Table 7-10. Cumulative Forces Resisting Pull Out from the TTS Salem Unit 1 FLB Conditions, T_{hot} = 601.8°F 7-22

Table 7-11. Cumulative Forces Resisting Pull Out from the TTS Salem Unit 1 FLB Conditions, T_{hot} = 613.1°F 7-23

Table 7-12. Summary of H* Calculations for Salem Unit 1 (Hot Leg Only) 7-24

Table 7-13. H* Summary Table 7-25

Table 9-1. First Order Equation Coefficients for the Variation of Contact Pressures Through Tubesheet 9-14

Table 9-3: Summary of Near Radius Leak Rate Ratio Results 9-15

Table 9-4: Summary of Mid Radius Leak Rate Ratio Results 9-15

Table 9-5: Summary of Peripheral Radius Leak Rate Ratio Results 9-15

Table 10-1. Radial Flexibilities Times Elastic Modulus (in./psi) 10-10

Table 10-2. Contact Pressure Influence Factors for Model F SG Tubes at 600°F 10-10

Table 10-3. Tubesheet Hole Diametral Dilation for R18C77 10-11

Table 11-1: Calculated H* & B* Depths by Radial Zone* 11-5

LIST OF FIGURES

Figure 3-1. Distribution of Indications in SG A at Catawba 2 3-4

Figure 3-2. Distribution of Indications in SG B at Catawba 2 3-4

Figure 3-3. Distribution of Indications in SG D at Catawba 2 3-5

Figure 6-1. Example Leakage Test Schematic 6-18

Figure 6-2. Example Tube Hydraulic Expansion Process Schematic 6-19

Figure 6-3. Example Tube Joint Leakage Test Configuration 6-20

Figure 6-4. Schematic for the Test Autoclave Systems for Leak Rate Testing 6-21

Figure 6-5. Example Tube Joint Sample Pullout Test Configuration 6-22

Figure 6-6. Loss Coefficient Values for Model F Leak Rate Analysis 6-23

Figure 7-1. Definition of H* Zones. 7-26

Figure 7-2. Finite Element Model of Model F Tubesheet Region 7-27

Figure 7-3. Contact Pressures for Normal Condition (Hot Leg) at Salem Unit 1 7-28

Figure 7-4. Contact Pressures for Normal Condition (High T_{avg}) at Salem Unit 1 7-29

Figure 7-5. Contact Pressures for SLB Faulted Condition at Salem Unit 1 7-30

Figure 7-6. Tube/Tubesheet Contact Pressures FLB Faulted Conditions 7-31

Figure 7-7. Tube/Tubesheet Contact Pressures FLB Condition – Salem Unit 1 Hot Leg, High T_{avg} 7-32

Figure 7-8. Location of Cracking Observed Along Partition Stub in EDF SGs 7-33

Figure 8-1. Change in Contact Pressure at the Bottom of the Tubesheet 8-8

Figure 8-2. Change in Contact Pressure at 16.9 Inches Below the TTS 8-8

Figure 8-3. Change in Contact Pressure at 10.515 Inches Below the TTS 8-9

Figure 8-4. Change in Contact Pressure at 6.00 Inches Below the TTS 8-10

Figure 8-5. Model F SLB Affected Loop HL Temperature Time History 8-11

Figure 9-1. Determination of H* 9-16

Figure 9-2. Determination of B* 9-17

Figure 9-3. Concepts for the Determination of B* 9-18

Figure 9-4. Schematic for the Determination of B* Parameters 9-19

Figure 9-5. First Order Linear Representation of Contact Pressure 9-20

Figure 9-6. Contact Pressure During Normal Operation (Model F) 9-21

Figure 9-7. Contact Pressure During SLB (2560 psi at 420°F) 9-22

Figure 9-8. NOp Contact Pressure vs. Depth Coefficients by Radius 9-23
Figure 9-9. SLB Contact Pressure vs. Depth Coefficients by Radius 9-24
Figure 9-10. Comparison of Contact Pressure Coefficients for NOp & SLB Conditions (Hot Leg) . 9-25
Figure 9-11. Elevation Below the TTS for Invariant Contact Pressure 9-26
Figure 9-12. TTS Contact Pressure for NOp & SLB Hot Leg Conditions 9-27
Figure 9-13. Viscosity of Water as a Function of Pressure 9-28
Figure 9-14. Viscosity of Water at 2560 psi as a Function of Temperature 9-29
Figure 9-15. Upper Bound B* for Salem Unit 1 SGs for No Change in Resistance 9-30
Figure 9-16. Nominal B* for Salem Unit 1 SGs for No Change in Leak Rate 9-31
Figure 9-17. Graphical Determination of B* Depth from Flow Resistance 9-32
Figure 9-18. TTS Contact Pressure for NOp & SLB Cold Leg Conditions 9-33
Figure 9-19: Sketch of Cracked Tube in Tubesheet Leading to Leakage Flowrate, Q_{OUT}. 9-34
Figure 9-20. Comparison of Models for Calculating the Crack Opening Area 9-34
Figure 9-21. Leak Rate Ratio as a Function of Axial Crack Length for a Tubesheet Radius of 60.248 inches at the Bottom of the Tube Sheet 9-35
Figure 9-22. Leak Rate Ratio as a Function of Circumferential Crack Length for a Tubesheet Radius of 60.248 inches at the Bottom of the Tube Sheet 9-35
Figure 10-1. Geometry of the Tube-to-Tubesheet Interface 10-12
Figure 10-2. Model for Initial Contact Pressure 10-12
Figure 10-3. Determination of Contact Pressure, Normal or Accident Operation 10-13
Figure 11-1. Comparison of H* and B* Hot Leg Results 11-6
Figure 11-2. Comparison of H* and B* Cold Leg Results 11-7

1.0 EXECUTIVE SUMMARY

Nondestructive examination indications of primary water stress corrosion cracking were found in the Westinghouse Model D5 Alloy 600 thermally treated steam generator tubes at the Catawba 2 nuclear power plant in the fall of 2004. Most of the indications were located in the tube-to-tubesheet welds with a few of the indications being reported as extending into the parent tube. In addition, a small number of tubes were reported with indications about 3/4 inch above the bottom of the tube within a region referred to as the tack-expansion, and multiple indications were reported in one tube at internal bulge locations in the upper third of the tubesheet. The tube end weld indications were dominantly axial in orientation and almost all of the indications were concentrated in one steam generator. Circumferential cracks were also reported at internal bulge locations in two of the Alloy 600 thermally treated steam generator tubes at the Vogtle 1 plant site in the spring of 2005. Internal tube bulges within the tubesheet are created in a number of locations as an artifact of the manufacturing process. Based on interpretations of requirements published by the NRC staff in GL 2004-01 and IN 2005-9, PSEG Nuclear LLC requested that a recommendation be developed for future examinations of the tubesheet regions of the steam generator tubes at Salem Unit 1. An evaluation was performed that considered the requirements of the ASME Code, Regulatory Guides, NRC Generic Letters, NRC Information Notices, the Code of Federal Regulations, NEI 97-06, and additional industry requirements. The conclusions of the technical evaluation are that:

1) the structural integrity of the primary-to-secondary pressure boundary is unaffected by tube degradation of any magnitude below a tube location-specific depth ranging from 2.25 to 7.05 inches depending on the tube leg and bundle zone being considered, designated as H*, and,
2) that the accident condition leak rate integrity can be bounded by a specified factor of the normal operation leak rate from degradation at or below a calculated distance of 6.83 inches, designated as B*, from the top of the 21.03 inch thick tubesheet, including degradation of the tube end welds.

These results follow from analyses demonstrating that the tube-to-tubesheet hydraulic joints make it extremely unlikely that any operating or faulted condition loads are transmitted below the H* elevation, and the contact pressure dependent leak rate resistance increases below the B* elevation within the tubesheet. The possibility of degradation at such locations in the Salem Unit 1 steam generator tubes may exist at sometime in the future based on the reported degradation at Catawba 2 and previously at Vogtle 1. The determination of the required engagement depth was based on results from finite element model structural analyses and a steam line break to normal operation comparative leak rate evaluation. It was also concluded that the evaluation of the conditions on the hot leg would always bound those for the cold leg with regard to leak rate performance. The cold leg requirements are greater than the hot leg requirements with regard to pullout resistance (the above numbers bound both). The structural length is slightly greater than that needed to restrict leak resistance to be the same during accident conditions at the center of the bundle. Application of the structural analysis and leak rate evaluation results to eliminate inspection and/or repair of tube indications in the region of the tube below the H* or B* elevation is interpreted to constitute a redefinition of the primary-to-secondary pressure boundary relative to the original design of the SG and requires the approval of the NRC staff through a license amendment. Using a maximum depth of 8.00 inches as the governing inspection length is sufficient to address all hot leg and cold leg considerations for both structural and leak rate integrity. This report also supports the implementation of a more conservative 17" inspection length permanent alternate repair criteria for Salem Unit 1 by providing responses to several NRC requests for additional information for another plant.

2.0 BACKGROUND

There has been extensive experience associated with the operation of SGs wherein it was believed, based on NDE, that throughwall tube indications were present within the tubesheet. The installation of the SG tubes usually involves the development of a short interference fit, referred to as the tack expansion, at the bottom of the tubesheet. The tack expansion was usually effected by a hard rolling process through October of 1979 and thereafter, in most instances, by the Poisson expansion of a urethane plug inserted into the tube end and compressed in the axial direction. The rolling process by its very nature is considered to be more intensive with regard to metalworking at the inside surface of the tube and would be expected to lead to higher residual surface stresses. A urethane expansion process was used during fabrication of the Salem Unit 1 SGs. The tube-to-tubesheet weld was then performed to create the ASME Code pressure boundary between the tube and the tubesheet.[1]

The development of the F* alternate repair criterion (ARC) in 1985-1986 for tubes hard rolled into the tubesheet was prompted by the desire to account for the inherent strength of the tube-to-tubesheet joint away from the weld and to allow tubes with degradation within the tubesheet to remain in service, Reference 1. The result of the development activity was the demonstration that the tube-to-tubesheet weld was superfluous with regard to the structural and leakage integrity of the rolled joint between the tube and the tubesheet. Once the plants were in operation, the structural and leakage resistance requirements for the joints were based on the plant Technical Specifications, and a means of demonstrating joint integrity that was acceptable to the NRC staff was delineated in Reference 1. License amendments were sought and granted for several plants with hard rolled tube-to-tubesheet joints to omit the inspection of the tube below a depth of about 1.5 inches from the top of the tubesheet. Similar criteria, designated as W*, were developed for explosively expanded tube-to-tubesheet joints in Westinghouse designed SGs in the 1991-1992 timeframe, Reference 2. The W* criteria were first applied to operating SGs in 1999 based on a generic evaluation for Model 51 SGs, Reference 3, and the subsequent safety evaluation by the NRC staff, Reference 4. However, the required engagement length to meet structural and leakage requirements was on the order of 5.2 to 7 inches because an explosively expanded joint does not have the same level of residual interference fit as that of a rolled joint. It is noted that the length of joint necessary to meet the structural requirements is not the same as, and is frequently shorter than, that needed to meet the leakage integrity requirements.

The post-weld expansion of the tubes into the tubesheet in the Salem Unit 1 SGs was effected by a hydraulic expansion of the tube instead of rolling or explosive expansion. The hydraulically formed joints do not exhibit the level of interference fit that is present in rolled or explosively expanded joints. However, when the thermal and internal pressure expansion of the tube is considered during normal operation and postulated accident conditions, appropriate conclusions regarding the need for the weld similar to those for the other two types of joint can be made. Evaluations were performed in 1996 of the effect of tube-to-tubesheet weld damage that occurred from an object in the bowl of a SG with tube-to-tubesheet joints similar to those in the Salem Unit 1 SGs, on the structural and leakage integrity of the joint, Reference 5. It was concluded in that evaluation that the strength of the tube-to-tubesheet joint is sufficient to prevent pullout in accordance with the requirements of the performance criteria of Reference 6 and that a significant number of tubes could be damaged without violating the performance criterion related to the primary-to-secondary leak rate during postulated accident conditions.

[1]The actual weld is between the Alloy 600 tube and weld buttering, a.k.a. cladding, on the bottom of the carbon steel tubesheet.

3.0 INTRODUCTION

Indications of cracking were reported based on the results from the nondestructive, eddy current examination of the steam generator (SG) tubes during the fall 2004 outage at the Catawba 2 nuclear power plant operated by the Duke Power Company, References 7, 8, and 9. The tube indications at Catawba were reported about 7.6 inches from the top of the tubesheet in one tube, and just above the tube-to-tubesheet welds in a region of the tube known as the tack expansion (TE) in several other tubes. Finally, indications were also reported in the tube end welds (TEWs), also known as tube-to-tubesheet welds, joining the tube to the tubesheet. The spatial distribution by row and column number is shown on Figure 3-1 for SG A, Figure 3-2 for SG B, and Figure 3-3 for SG D at Catawba 2. There were no indications in SG C. The Catawba 2 plant has Westinghouse designed, Model D5 SGs fabricated with Alloy 600TT (thermally treated) tubes. There is the potential for additional tube indications similar to those already reported at Catawba 2 within the tubesheet region to be reported during future inspections.

It was subsequently noted that an indication was reported in SG tubes at the Vogtle Unit 1 plant operated by the Southern Nuclear Operating Company (Reference 10). The Vogtle SGs are of the Westinghouse Model F design with slightly smaller, diameter and thickness, A600TT tubes.

Note: No indications of this type were found during the planned inspections of the Braidwood 2 SG (Model D5 SGs) tubes in April 2005, a somewhat similar inspection of the tubes in two SGs at Wolf Creek (Model F SGs) in April 2005, or an inspection of the tubes at Comanche Peak 2 (Model D5 SGs) in the spring of 2005. Also, no indications of this type were found during similar inspections at Byron 2 (Model D5 SGs) and Vogtle 2 (Model F SGs) in the fall of 2005. Additionally, 1054 bulges were examined in 2 SGs at Millstone Unit 3 (Model F SGs) in the fall of 2005 with no crack-like indications identified.

The SGs for all four Model D5 plant sites were fabricated in the 1978 to 1980 timeframe using similar manufacturing processes with a few exceptions. For example, the fabrication technique used for the installation of the SG tubes at Braidwood 2 would be expected to lead to a much lower likelihood for crack-like indications to be present in the region known as the tack expansion relative to Catawba 2 because a lower stress urethane expansion process for effecting the tack expansions was adopted prior to the time of the fabrication of the Braidwood 2 SGs. The tack expansions in the steam generator tubes at Salem Unit 1 were completed by a urethane expansion process (similar to Braidwood Unit 2) as they were shipped in 1983.

The findings in the Catawba 2 and Vogtle 1 SG tubes present three distinct issues with regard to future inspections of A600TT SG tubes which have been hydraulically expanded into the tubesheet:

1) indications in internal bulges within the tubesheet,
2) indications at the elevation of the tack expansion transition, and
3) indications in the tube-to-tubesheet welds, including some extending into the tube.

The scope of this document is to:

a) address the applicable requirements, including the original design basis, Reference 11, and regulatory issues, Reference 12, and

b) provide analysis support for technical arguments to limit inspection of the tubesheet region to a distance of 8 inches below the top of the tubesheet below which degradation of any extent would not adversely affect SG performance criteria

These results follow from analyses demonstrating that the tube-to-tubesheet hydraulic joints make it extremely unlikely that any operating or faulted condition loads are transmitted below the H* elevation, and that the tube-to-tubesheet contact leak rate resistance increases below the B* elevation within the tubesheet. The determination of the required engagement depth was based on the use of finite element model structural analyses and of a bounding leak rate evaluation based on the change in contact pressure between the tube and the tubesheet between normal operation and postulated accident conditions. The results provide the technical rationale to eliminate inspection of the region of the tube below the H* or B* elevation. Such an approach is interpreted to constitute a redefinition of the primary-to-secondary pressure boundary relative to the original design of the SG and requires the approval of the NRC staff through a license amendment.

A similar type of Technical Specification change was approved, on a one-time basis, to limit inspections of the Wolf Creek Model F and Braidwood 2 Model D5 SGs during the spring 2005 inspection campaigns, for example see References 13 and 14 respectively. Subsequent approvals were also obtained for use at Byron 2 and Vogtle 2 for their fall 2005 inspection campaigns, Reference 15 for example for the latter. This report was prepared to justify the specialized probe, e.g., RPC (rotating probe coil), exclusion zone to the portion of the tube below specific elevations from the top of the tubesheet based on meeting the structural and leak rate performance criteria for both the hot and cold leg, and to provide the necessary information for a detailed NRC staff review of the technical basis for that request. The major difference between the current evaluation of the Salem Unit 1 SGs and prior applications is the identification of an inspection length which is above the neutral plane instead of simply using an arbitrary, bounding value of 17 inches from the top of the tubesheet.

The H* values were determined to assure meeting the structural performance criteria for the operating SG tubes as delineated in NEI 97-06, Revision 2, Reference 6. The B* values were determined based on meeting the accident condition leak rate performance criteria. Compliance is based on demonstrating both structural and leakage integrity during normal operation and postulated accident conditions. The structural model was based on standard analysis techniques and finite element models as used for the original design of the SGs and documented in numerous submittals for the application of criteria to deal with tube indications within the tubesheet of other models of Westinghouse designed SGs with tube-to-tubesheet joints fabricated by other techniques, e.g., explosive expansion.

All full depth expanded tube-to-tubesheet joints in Westinghouse-designed SGs have a residual radial preload between the tube and the tubesheet. Early vintage SGs involved hard rolling which resulted in the largest magnitude of the residual interface pressure. Hard rolling was replaced by explosive expansion which resulted in a reduced magnitude of the residual interface pressure. Finally, hydraulic expansion replaced explosive expansion for the installation of SG tubes, resulting in a further reduction in the

residual interface pressure. In general, it was found that the leak rate through the joints in hard rolled tubes is insignificant. Subsequent testing demonstrated that the leak rate resistance of explosively expanded tubes was not as great as that of hard rolled tubes and prediction methods based on empirical data to support theoretical models were developed to deal with the potential for leakage. The same approach was followed to develop a prediction methodology for hydraulically expanded tubes. However, the model has been under review since its inception, with the intent of verifying its accuracy because it involved analytically combining the results from independent tests of leak rate through cracks with the leak rate through the tube-to-tubesheet crevice. The leak rate model associated with the initial development of H* to meet structural performance criteria is such a model; technical acceptance could be time consuming since it has not been previously reviewed by the NRC staff. An alternative approach, provided in this report, was developed for application at Salem Unit 1 from engineering expectations of the relative leak rate between normal operation and postulated accident conditions based on a first principles engineering evaluation.

An executive summary, background and introduction discussion are provided in Sections 1.0, 2.0 and 3.0 of this report. A summary discussion is provided in Section 4.0. Section 5.0 addresses plant operating conditions at Salem Unit 1. Section 6.0 discusses the tube pullout and leakage test programs that are applicable to the Model F SGs at Salem Unit 1. A summary of the conclusions from the structural analysis of the joint is provided in Section 7.0. The leak rate analysis is provided in Section 8.0. A determination of the requisite inspection depth based on leak rate considerations is provided in Section 9.0. A review of the qualitative arguments used by the NRC Staff for the tube joint inspection length approved for other plants is discussed in Section 10.0. Finally, conclusions from the structural and leak rate evaluations are contained in Section 11.0 of this report.

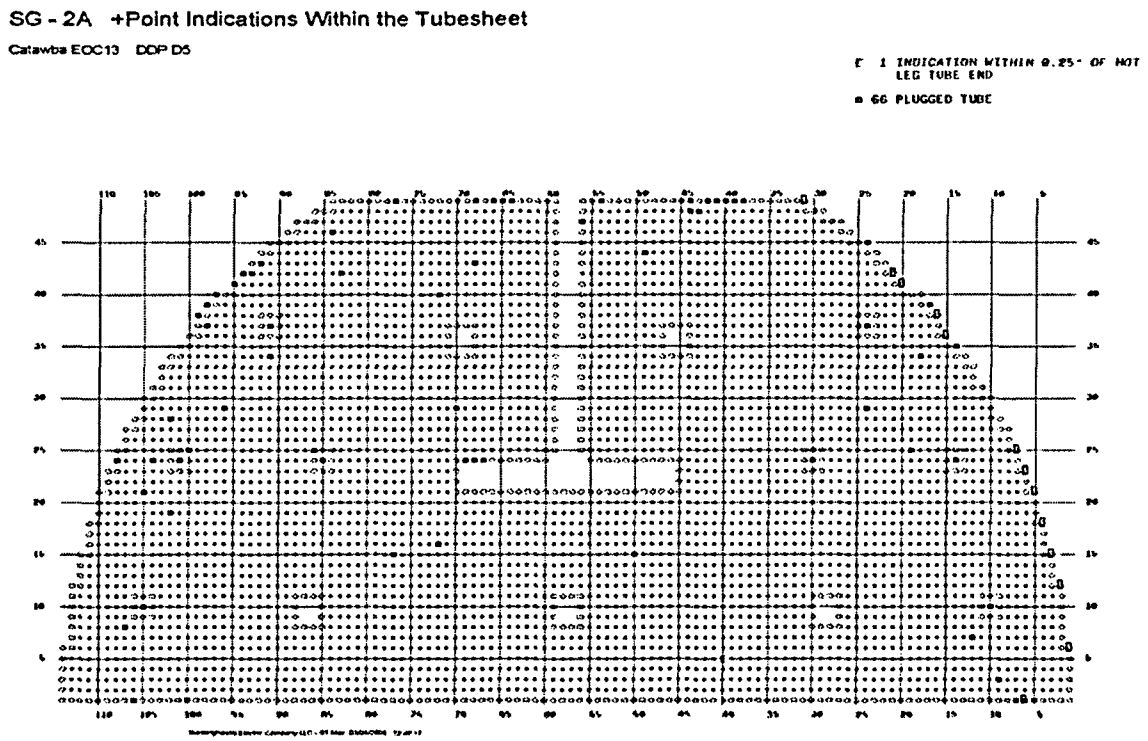

Figure 3-1. Distribution of Indications in SG A at Catawba 2

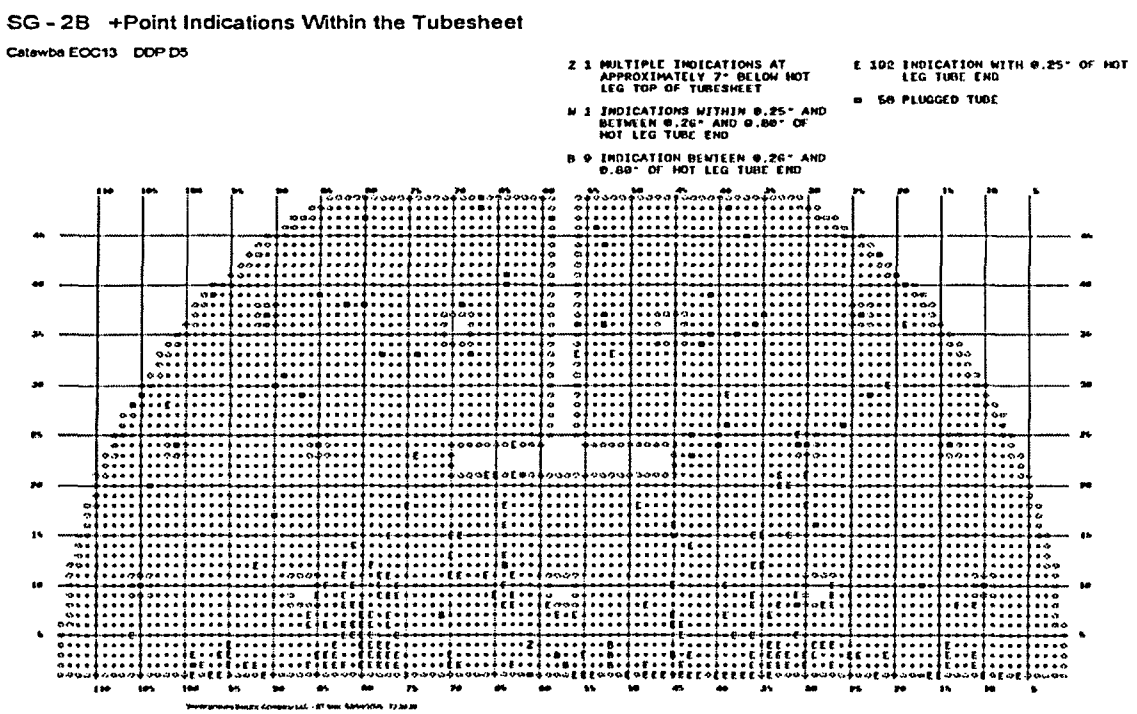

Figure 3-2. Distribution of Indications in SG B at Catawba 2

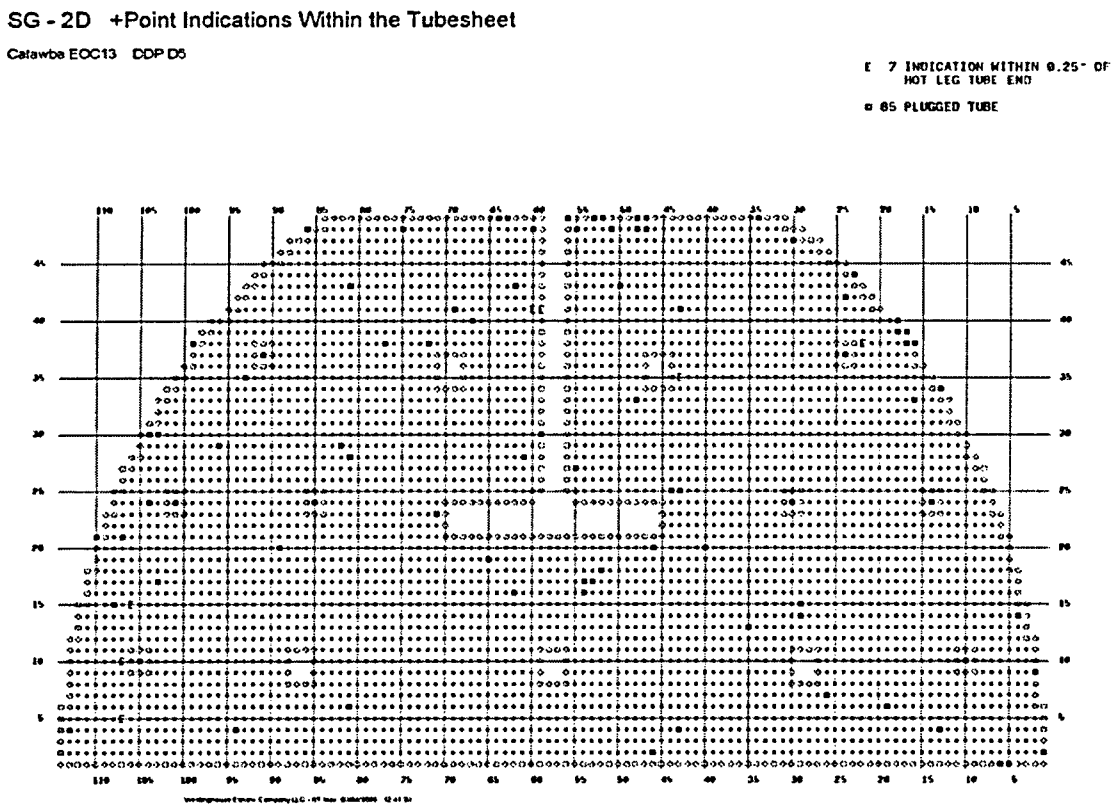

Figure 3-3. Distribution of Indications in SG D at Catawba 2

4.0 SUMMARY DISCUSSION

An evaluation has been performed that considered the requirements of the ASME Code, Regulatory Guides, NRC Generic Letters, NRC Information Notices, the Code of Federal Regulations, NEI 97-06, and additional industry requirements. The conclusion of the technical evaluation is that:

1) the structural integrity of the primary-to-secondary pressure boundary is unaffected by tube degradation of any magnitude below a tube location-specific depth ranging from 2.25 to 7.05 inches, designated as H*, and,
2) that the accident condition leak rate integrity can be bounded by twice the normal operation leak rate from degradation of any magnitude below 6.83 inches from the top of the nominally 21.03 inch thick tubesheet, including degradation of the tube end welds, designated as B*.

These results follow from analyses demonstrating that the tube-to-tubesheet hydraulic joints make it extremely unlikely that any operating or faulted condition loads that adversely impact structural integrity are transmitted below the H* elevation.

However, the leak rate during postulated accident conditions would be expected to be less than that during normal operation for indications near the bottom of the tubesheet (including indications in the tube end welds) based on the observation that while the driving pressure increases by about a factor of almost two, the flow resistance increases because the tube-to-tubesheet contact pressure also increases. Depending on the depth within the tubesheet, the relative increase in resistance could easily be larger than that of the pressure potential. Therefore, the leak rate under normal operating conditions could exceed its allowed value before the accident condition leak rate would be expected to exceed its allowed value. This approach is termed an application of the "bellwether principle." The evaluations were performed to specifically determine relative changes in the leak rate resistance as a function of tube location from the center of the tubesheet and degradation distance from the top of the tubesheet. The assessment envelopes postulated circumferential cracking of the tube or the tube-to-tubesheet weld that is 100% deep by 360° in extent because it is based on the premise that the tube and weld are not present below the analyzed elevations.

Based on the information summarized above, no inspection of the tube-to-tubesheet welds, tack roll region or bulges below the distance determined to have the potential for safety significance as specified in Reference 16, i.e., the H* depths, would be considered to be the minimum distance to be necessary to assure compliance with the structural requirements for the SGs. In addition, based on the results from consideration of application of the bellwether principle regarding potential leakage during postulated accident conditions, inspection depths as a function of tube location can be established using the results from these analyses but are not planned to be utilized by PSEG Nuclear LLC. Note that both B* and H* depths vary dependent upon the radial distance from the SG vertical centerline.

The length determined for structural compliance purposes bounds the required length for leak rate compliance as discussed in Sections 8.0 and 9.0 of this report. For example, compare the results in Table 7-12 to those in Table 9-2. The application of the bellwether approach to the leak rate analysis as described in Section 8.1 of this report negates the need to consider specific leak rates because it relies only on the relative magnitude of the joint contact pressures between the tube and the tubesheet.

5.0 OPERATING CONDITIONS

Salem Unit 1 is a four-loop nuclear power plant with Westinghouse designed and fabricated Model F SGs; there are 5626 tubes in each SG. The design of these SGs includes Alloy 600 thermally treated (A600TT) tubing, full-depth hydraulically expanded tubesheet joints, and broached hole quatrefoil tube support plates constructed of stainless steel.

5.1 BOUNDING OPERATING CONDITIONS

Values that bound the current Salem Unit 1 SG thermal and hydraulic parameters during normal operation are tabulated below:

Parameter and Units		Bounding Operating Conditions (1)
Power – NSSS	MWt	3471
Reactor Vessel Outlet Temperature	°F	601.8
Reactor Coolant System Pressure	psig	2235
SG Steam Temperature	°F	510.2
SG Steam Pressure	psig	731
Steam Line Break Pressure	psig	2560
(1) Reference 17		

5.2 FAULTED CONDITIONS

In addition to the RG 1.121 criteria, it is necessary to satisfy the updated final safety analysis report (UFSAR) accident condition assumptions for primary-to-secondary leak rates. Calculated primary-to-secondary side leak rate during postulated events should: 1) not exceed the total charging pump capacity of the primary coolant system, and 2) be such that the off-site radiological dose consequences do not exceed Title 10 of the Code of Federal Regulations (10 CFR) Part 100 guidelines.

The accident condition primary-to-secondary leakage must be limited to acceptable values established by plant specific UFSAR evaluations. Pressure differentials associated with a postulated accident condition event can result in leakage from a throughwall crack through the interface between a hydraulically expanded tube in the tubesheet and the tube hole surface. Therefore, a steam generator leakage evaluation for faulted conditions is provided in this report. The accidents that are affected by primary-to-secondary leakage are those that include, in the activity release and off-site dose calculation, modeling of leakage and secondary steam release to the environment. steamline break (SLB) is the limiting condition; the reasons that the SLB is limiting are:

1) the SLB primary-to-secondary leak rate in the faulted loop is assumed to be greater than the operating leak rate because of the sustained increase in differential pressure, and
2) leakage in the faulted steam generator is assumed to be released directly to the environment.

For evaluating the radiological consequences due to a postulated SLB, the activity released from the affected SG (which is connected to the broken steam line) is released directly to the environment. The unaffected steam generators are assumed to continually discharge steam and entrained activity via the safety and relief valves up to the time when initiation of the RHR system can be accomplished. The radiological consequences evaluated, based on meteorological conditions, usually assume that all of this flow goes to the affected SG. With the analytically determined level of leakage, the resultant doses are expected to be within the guideline values of 10 CFR 100.

Structurally, the postulated SLB event is the limiting loading condition for the hot leg of the tube bundle. The postulated feedline break event is the limiting loading condition for the cold leg of the tube bundle for Zones B, C and D. The normal operating condition is the limiting loading condition for the cold leg of the tube bundle in Zone A. See Section 7.0 of this report.

6.0 STEAM GENERATOR TUBE PULLOUT AND LEAKAGE TEST PROGRAM DISCUSSION

While the tube material and tube installation into the tubesheet technique are similar between the Westinghouse Model F and Model D5 SGs, there are also differences between the designs with regard to the tube size, thickness, number of tubes and tube pitch. Data are available with regard to pullout and leak rate testing for each of the SG geometries. The original testing of Reference 17 was performed to investigate postulated extreme effects on the tube-to-tubesheet weld from a loose part on the primary side of one Model F SG. These data were also used to support the model specific development of the required H* length and to characterize the leak rate from throughwall tube indications within the tubesheet as a function of the contact pressure between the tube and the tubesheet, e.g., Reference 19 was originally written for the Wolf Creek SGs. The testing also provides valuable information regarding the calculation of the 8 inch inspection length once a relative SLB to NOp leak rate has been identified. Pullout and leak rate data were also available from similar testing performed using Model D5 specific geometry, References 20. The data from both sets of testing programs were combined to support the development of the inspection criteria delineated in this report for the Salem Unit 1 Model F SGs.

- The results from strength tests were used to establish the joint lengths needed to meet the structural performance criteria during normal operation and postulated accident conditions, the required engagement length being 7.05 inches and designated as H*. The inherent strength of the joint coupled with the results from a finite element model of the loading conditions is used to calculate the required H* values for Model D5, Model 44F, and Model F SGs.
- The results from leak rate tests were used to support the methodology to quantify the leak rate during postulated accident conditions as a function of the leak rate during normal operation. The required engagement length to meet a specific leak rate objective is designated as B* and is 6.83 inches so that the leak rate expected during a postulated accident event is no more than twice that during normal operation.

Data from the test programs for the Model F SGs, Reference 21, directly supports the determination of both the H* and the 8 inch inspection length values for the SG tubes. The testing programs had two purposes:

1) To characterize the strength of the tube-to-tubesheet joints in Model F SGs during normal operation, e.g., 600°F, and under postulated accident conditions, and,
2) To characterize the leak resistance of the tube-to-tubesheet joints in Model F SGs during normal operation and under postulated accident conditions.

Similar testing was performed using specimens designed to simulate installed tubes in Model D5 SGs to develop parallel criteria for other plants. The independent testing programs that were conducted to characterize the joint strength and leak rate characteristics for Model 44F, Model F and Model D5 SGs are discussed separately in the following sections.

6.1 TUBE PULLOUT RESISTANCE PROGRAMS

The purpose of the tube pullout testing discussed below was to determine the resistance of the simulated Model F, D5 and 44F tube-to-tubesheet joints to pullout at temperatures ranging from [.]a,c,e

6.1.1 Model F Tube Pullout Test Program and Results

Mechanical loading, [ ]a,c,e All of the test results are listed in Table 6-5.

[ ]a,c,e.

6.1.2 Model D5 Tube Pullout Test Program and Results

The Model D5 pullout test samples were fabricated with the same processes as used for the leakage tests, refer to Figure 6-5, and described later in this section. The tube expansion tool used in the program was a factory device, modified to achieve an expansion ranging of from three to seven inches.

Model D5 hydraulic expansion joints with nominal axial lengths of [ ]a,c,e Generally, but not always, the larger-deflection load value is greater than the knee value. In this program [ ]a,c,e was used to obtain the input information for calculation of the H* values for the plants (see Table 6-7). The pullout load from these plots simply provides one of the inputs used to calculate H*. The other variables in the H* calculations include tubesheet bending (causing the tubesheet hole to dilate and/or contract depending on the distance of a certain point below the tubesheet top), the thermal growth mismatch effect (owing to the differential thermal growth between the Alloy 600TT tube and the carbon steel tubesheet and the "differential pressure tightening" of the tube within the tubesheet.

Mechanical loading, or pullout, tests on samples of the tube joints were run [

]a,c,e

[

]a,c,e

6.1.3 Model 44F Tube Pullout Test Program and Results

Mechanical loading (pullout) tests on samples of the tube joints were run on a mechanical testing machine, configured so that the tube could be pulled out of the simulated tubesheet (however, it is pulled through a limited range for testing purposes). In this test series, the testing was run with non-pressurized tubes. In previous testing, pressurized tubes were also included in the test matrix. Typically the resistive force for the pressurized case is so high that the tube would yield and break rather than being able to obtain data on pullout forces. Therefore, for this test series, just the non-pressurized tubes were used.

Three tests were conducted at room temperature, three at 400°F, and three at 600°F. Each of the groupings of three tests consisted of one test with an engagement length of 3 inches, one at 5 inches, and one at 7 inches (all values nominal). Following expansion of the non-welded Alloy 600 tubes at a pressure of 31 ksi (e.g., expansions for one plant's 2 SGs were in the range of 31 to 34 ksi), the samples were subject to a high temperature soak to simulate the stress relief of the channelhead-to-tubesheet weld. The specimens were then subjected to pull testing to determine the load required to effect a displacement of 0.25 inch of the tube in the tubesheet. The average force per inch required to produce a 0.25 inch displacement was 1172 lb per inch with a standard deviation of 558 lb per inch. Using the average value results in a calculated pullout force of 614 lb per inch. Refer to Reference 24 for data from several samples.

In the 600°F pullout tests, the lowest value for small displacement was 1310 lbs. The average small displacement load for these 600°F samples was 2044 lbs. The maximum load at 0.25 inch displacement for these three 600°F samples was 7687 lbs, the minimum load was 6248 lbs. See Table 6-8.

6.2 LEAK RATE TESTING PROGRAMS

The purpose of the testing programs was to provide quantified data with which to determine the [

]a,c,e As discussed in detail in Section 8.0, the analytical model for the leak rate is referred to as the Darcy or Hagen-Poiseuille formulation. The volumetric flow is a function of the pressure potential, the inverse of the crevice length, the inverse of the fluid viscosity, and the inverse of a resistance term characteristic of the geometry of the tube-to-tubesheet joint and referred to as the loss coefficient. Thus, the purpose of the

testing programs is to obtain data with which to determine the loss coefficient. Data were available from leak rate test programs that independently addressed the Model F and the Model D5 tube-to-tubesheet joints:

a. The Model F tube joint leakage resistance program involved tests at [ ]a,c,e

b. The Model D5 tube joint leakage resistance program involved tests at [ ]a,c,e

The Model F program and results are described in Sections 6.2.1 and 6.2.2, followed by the description and results of the Model D5 program in Sections 6.2.3 and 6.2.4.

6.2.1 Model F Tube Joint Leakage Resistance Program

A total of [ ]a,c,e The leakage resistance data were calculated for the test conditions listed in Table 6-1.

6.2.1.1 Model F Test Specimen Configuration

The intent of the test samples was to model key features of the Model F tube-to-tubesheet joint for [ ]a,c,e. The following hardware was used:

A Model F tubesheet simulating collar which mimicked the radial stiffness of a Model F tubesheet unit cell with an outside diameter of approximately [ ]a,c,e. The length of the test collars was [ ]a,c,e thickness of the steam generator tubesheet. This allowed for the introduction and collection of leakage in unexpanded sections of the tube, while retaining conservative or typical hydraulic expansion lengths. The collars were drilled to the nominal design value inside diameters with the surface finish based on drawing tolerances. In addition, the run-out tolerance for the collar drilling operation was held to within [ ]a,c,e

[

]a,c,e.

Model F A600TT tubing with a yield strength approximately the same as that of the tubes in the operating plants, which ranges from [ ]a,c,e was used. The tubing used was from a certified heat and lot conforming to ASME SB163, Section III Class 1 and was maintained in a Quality Systems-controlled storeroom prior to use.

The intent of the leakage portion of the test program was to determine the leakage resistance of simulated Model F tube-to-tubesheet joints, disregarding the effect of the tube-to-tubesheet weld and the [

]a,c,e, see Figure 6-1. The welds were a feature of the test specimen design and made no contribution to the hydraulic resistance.

6.2.1.2 Model F Test Sample Assembly

The SG factory tube installation drawing specifies a [

]a,c,e, to facilitate the tube weld to the cladding on the tubesheet face and it was omitted from the test. Following welding of the tube to the tubesheet, a full-length hydraulic expansion of the tube into the tubesheet is performed. The hydraulic expansion pressure range for the Model F SGs was approximately [ ]a,c,e The majority of the test samples were expanded using a specified pressure of [ ]a,c,e to conservatively bound the lower expansion pressure limit used for SG fabrication.

The tube expansion tool used in the factory consisted of a pair of seals, spaced by a tie rod between them. The hydraulically expanded zone was positioned relative to the lower surface of the tubesheet, overlapping the upper end of the tack expanded region. It extended to within a short distance of the upper surface of the tubesheet. This produced a hydraulically expanded length of approximately [ ]a,c,e inch nominal tubesheet thickness. The majority of the test specimens were fabricated using [ ]a,c,e Previous test programs which employed a segmented approach to expansion confirmed the expectation that uniform results from one segment to the next would result. This approach produced the desired expansion pressures for a conservative length of [ ]a,c,e inch-expanded length being simulated. The remaining length of tube was expanded to the pressure at which the expansion bladder failed, usually between [.]a,c,e These samples are described as "Segmented Expansion" types. A tube expansion schematic is shown on Figure 6-2.

Data were also available from a small group of the test samples that had been previously fabricated using a [ ]a,c,e tool which had been fabricated expressly for such tests. These samples were described as "Full Depth Expansion" types. The expansion method with regard to the segmented or full length aspect does not have a bearing on the test results.

6.2.2 Model F Leakage Resistance Tests

The testing reported herein was performed according to a test procedure which outlined two types of leak tests as follows:

1) Model F elevated temperature primary-to-secondary leak tests were performed using an [ ]a,c,e These tests were performed following the room temperature primary-to-secondary side leak tests on the chosen samples. The test results showed a [ ]a,c,e

2) Model F room temperature primary-to-secondary side leak tests were performed on all test samples, [ ]a,c,e These tests were performed following the elevated temperature primary-to-secondary side leak tests on the chosen samples.

6.2.2.1 Model F Leak Test Results

The leak tests on segmented expansion collars averaged [

]^{a,c,e} (As a point of reference, there are approximately 75,000 drops in one gallon.) Leakage data were also recorded at room temperature conditions to provide input for the low contact pressure portion of the flow loss coefficient-versus-contact pressure correlation.

6.2.3 Model D5 Tube Joint Leakage Resistance Program

A total of [

]^{a,c,e}

The lower bound leakage resistance distribution for the collars with the nominal tubesheet hole diameter was used in the present leakage evaluation. This lower bound leakage resistance was made using data for the test conditions shown in Table 6-2 below combined with the Model F leak test results discussed in Section 6.1.

6.2.3.1 Model D5 Test Specimen Configuration

The intent of the test samples was to model key features of the Model D5 tube-to-tubesheet joint for [ ]^{a,c,e} The following hardware was used:

1) A Model D5 tubesheet simulating collar matching the radial stiffness of a Model D5 tubesheet unit cell, utilizing an appropriate outside diameter of approximately [

]^{a,c,e}

Model D5 Tubing with an average yield strength for the SG Alloy 600 tubing in the Model D plants is [ ]^{a,c,e}. The Alloy 600 tubing used for these tests was from heats conforming to ASME SB163, Section III Class 1. It was obtained from a Quality Systems-controlled Storeroom.

The intent of the leakage portion of the test program was to determine the leakage resistance of simulated Model D5 tube-to-tubesheet joints, disregarding the effect of the [
]a,c,e

Tube-to-tubesheet stimulant samples of the Model D5 configuration were designed and fabricated. The steam generator factory tubing drawing specifies a [

]a,c,e

The hydraulic expansion pressure range for the Model D5 steam generators was [
]a,c,e This value conservatively bounds the lower expansion pressure limit used for the Model D5 steam generators. Refer to Figure 6-3 for the details of the configuration for the leak test. The test equipment consisted of a make-up tank (MUT), primary water autoclave (AC1) and a secondary autoclave (AC2) connected by insulated pressure tubing. Two specimens were installed into the secondary autoclave to minimize setup time and variability across test runs. AC1 was run with deoxygenated primary water containing specified amounts of boron, lithium and dissolved hydrogen. The primary chemistry conditions were controlled in the MUT and a pump and backpressure system allowed the primary water to re-circulate from the MUT to the AC1. The primary autoclave had the normal controls for heating, monitoring pressure and safety systems including rupture discs. Figure 6-4 shows the entire test system with key valves and pressure transducers identified. In addition to the normal controls for heating, monitoring pressure and maintaining safety, the secondary autoclave was outfitted water cooled condensers that converted any steam escaping from the specimens into room temperature water. The pressure in the secondary side (in the main body of AC2, was monitored by pressure transducers. For most tests, the leakage was collected in a graduated cylinder on a digital balance connected to a computer so that the amount of water could be recorded as a function of time. For some normal operating tests, the leakage was calculated based on changes in the secondary side pressure. All relevant autoclave temperatures and pressures were recorded with an automatic data acquisition system at regular time intervals.

6.2.3.2 Model D5 Test Sample Assembly

The assumption that pull-out resistance is distributed uniformly through the axial extent of the joint is an adequate technical approach. The pullout resistance is asymptotic to some large value, the form of the relation is one minus an exponential to a negative multiple of the length of engagement. For short engagement lengths, say up to 5 to 8 inches, the linear approximation is sufficient. Extrapolations to higher pullout resistance for longer lengths could be non-conservative except for the fact that the pullout strength of the shorter lengths exceeds the structural performance criteria.

6.2.4 Model D5 Leakage Resistance Tests

For the Model D5 testing, primary-to-secondary leak tests were performed on all test samples, using simulated primary water as a pressurizing medium. Refer to Figure 6-3. [

]a,c,e

[

]^{a,c,e}, to simulate a perforation of the tube wall due to corrosion cracking. All of the elevated temperature primary-to-secondary side leak tests were performed using an [

]^{a,c,e} as the pressurizing/leakage medium. In the case of 800 psid back pressure tests, the leakage was collected in the autoclave as it issued from the tube-to-collar crevice. In the remainder of the autoclave tests, the leakage was collected in the autoclave as it issued from the tube-to-collar crevice but it was piped to a condenser/cooler and weighed on an instrumented scale.

6.2.4.1 Model D5 Leak Test Results

The leakage rates for the Model D5 600°F normal operating and accident pressure differential conditions were similar to the respective Model F values. Leakage ranged from [

]^{a,c,e} Leakage data were also recorded at room temperature conditions to provide input for the low contact pressure portion of the flow loss coefficient-versus-contact pressure correlation.

6.3 LOSS COEFFICIENT ON CONTACT PRESSURE REGRESSION

A logarithmic-linear (log-linear) regression and an uncertainty analysis were performed for the combined Model F SG data. Figure 6-6 provides a plot of the loss coefficient versus contact pressure with the linear regression trendline for the combined data represented as a thick, solid black line. The regression trendline is represented by the log-linear relation,

$$\ln(K) = b_0 + b_1 P_c \tag{1}$$

where b_0 = the ln(K) intercept of the log-linear regression trendline, and,

b_1 = the slope of the log-linear regression trendline.

In conclusion, the log-linear fit to the Model F loss coefficient data follow a relation of the form,

$$K = e^{b_0 + b_1 P_c}, \tag{2}$$

where the absolute leak rates per se are not used in the determination the 8 inch inspection length and the confidence curve on the charts is provided for information only. Since the 8 inch inspection length criteria is based on the ratio of the SLB leak rate to the NOp leak rate it is not significantly sensitive to changes in the correlation slope or intercept.

Table 6-1. Model F Leak Test Program Matrix

a,c,e

* The loss coefficient for leakage analyses is treated as an empirical parameter and is obtained by correlating the elevated temperature test data, which is representative of normal operation and accident conditions. The single set of room temperature data used at the low end of the contact pressure range conservatively extends the elevated temperature data to low contact pressure conditions.

Table 6-2. Model D5 Leak Test Program Matrix

a,c,e

Table 6-3. Model F Leak Rate Testing Data

a,c,e

Table 6-4. Model D5 Experimental Loss Coefficients a,c,e

Table 6-4. Model D5 Experimental Loss Coefficients (Cont.)

a,c,e

Table 6-5. Model F 0.25 Inch Displacement Pullout Test Data

a,c,e

Table 6-6. Model F Small Displacement Data at 600°F

a,c,e

Table 6-7. Model D5 0.25 Inch Displacement Pullout Test Data

a,c,e

Table 6-8. Model 44F 0.25 Inch Displacement Pullout Test Data

a,c,e

a,c,e

Figure 6-1. Example Leakage Test Schematic

a,c,e

Figure 6-2. Example Tube Hydraulic Expansion Process Schematic

a,c,e

Figure 6-3. Example Tube Joint Leakage Test Configuration

a,c,e

Figure 6-4. Schematic for the Test Autoclave Systems for Leak Rate Testing

a,c,e

Figure 6-5. Example Tube Joint Sample Pullout Test Configuration

a,c,e

[ ]

Figure 6-6. Loss Coefficient Values for Model F Leak Rate Analysis

7.0 STRUCTURAL ANALYSIS OF TUBE-TO-TUBESHEET JOINT

This section summarizes the structural aspects and analysis of the entire tube-to-tubesheet joint region. The tube end weld was originally designed as a pressure boundary structural element in accordance with the requirements of Section III of the ASME (American Society of Mechanical Engineers) Boiler and Pressure Vessel Code, Reference 11. The construction code for the Salem Unit 1 replacement SGs was the 1971 edition through the Summer 1973 addenda. This means that there were no strength considerations made with regard to the expansion joint between the tube and the tubesheet, including the tack expansion regardless of whether it was achieved by rolling or Poisson expansion of a urethane plug.

An extensive empirical and analytical evaluation of the structural capability of the as-installed tube-to-tubesheet joints based on considering the weld to be absent was performed specifically for the Salem Unit 1 Model F SGs and the results are reported below. Typical Model F hydraulic expansion joints with lengths comparable to those being proposed in what follows for limiting inspection examination requirements were tested for pullout resistance strength at temperatures ranging from 70 to 600°F. The results of the tests coupled with those from finite element evaluations of the effects of temperature and primary-to-secondary pressure on the tube-to-tubesheet interface loads have been used to demonstrate that engagement lengths of approximately 2.25 to 7.05 inches were sufficient to equilibrate the axial loads resulting from consideration of 3 times the normal operating and 1.4 times the limiting accident condition pressure differences. The variation in required engagement length is a function of tube location, i.e., row and column, and decreases away from the center of the SG where the maximum value applies. The tubesheet bows, i.e., deforms, upward from the primary-to-secondary pressure difference and results in the tube holes becoming dilated above the neutral plane of the tubesheet, which is slightly below the mid-plane because of the effect of the tensile membrane stress from the pressure loading. The amount of dilation is a maximum near the radial center of the tubesheet (restricted by the divider plate) and diminishes radially with increasing radius outward. Moreover, the tube-to-tubesheet joint becomes tighter below the neutral axis and is a maximum at the bottom of the tubesheet[1]. In conclusion, the need for the weld is obviated by the interference fit between the tube and the tubesheet. Axial loads are not transmitted to the portion of the tube below the H* distance during operation or faulted conditions, by factors of safety of at least 3 and 1.4 respectively. Inspection of the tube below the H* distance including the tube-to-tubesheet weld is not technically necessary. Also, if the expansion joint were not present, there would be no effect on the pullout strength if axial cracks are present, and tubes with circumferential cracks up to about 180° by 100% deep would have sufficient pullout strength to meet the nominal ASME Code structural requirements, based on the margins of safety reported in Reference 25.

An examination of Table 7-7 through 7-9 illustrates that the holding power of the tube-to-tubesheet joint at a depth of 8 inches from the top of the tubesheet. Note that the radii reported in these tables were picked to conservatively represent the entire radial zones of consideration as defined on Figure 7-1. For example, Zone C has a maximum radius of 12 inches, however, in order to establish an H* value that was conservative throughout the zone, the tube location for which the analysis results were reported, is at a radius of 4.016 inches. Likewise for tubes in Zone C under the heading 30.2 inches the basis for the calculation was a tube at a radius of 20.5 inches. The purpose of this discussion is to illustrate the conservatism associated with the holding power of the joint above the neutral plane of the tubesheet, and

[1] There is a small reversal of the bending stress beyond a radius of about 50 inches because the support ring prevents rotation and the hole dilation is at the bottom of the tubesheet.

to identify the proper tube radii for consideration. In the center of the tubesheet the incremental holding strength in the 4.00 inch range from 4.00 to 8.00 inches below the top of the tubesheet is 2975.8 lbf during normal operation (hot leg), which meets the performance criterion of 3.0 ΔP within the first 2.33 inch of engagement above 8 inches. The performance criterion for 1.4·SLB ΔP is met by the first 1.62 inch of engagement above 8 inches. At a radius of 45.5 inches the corresponding length of engagement needed is 1.76 inches above 8 inches during normal operating conditions.

7.1 EVALUATION OF TUBESHEET DEFLECTION EFFECTS FOR TUBE-TO-TUBESHEET CONTACT PRESSURE

A finite element model was developed for the Model F tubesheet, channel head, and shell region to determine the tubesheet hole dilations in the Salem Unit 1 steam generators. [

]^{a,c,e} loads in the tube.

7.1.1 Material Properties and Tubesheet Equivalent Properties

The tubes in the Salem Unit 1 SGs were fabricated of A600TT material. Summaries of the applicable mechanical and thermal properties for the tube material are provided in Table 7-1. The tubesheets were fabricated from SA-508, Class 2a, material for which the properties are listed in Table 7-2. The shell material is SA-533 Grade A Class 2, and its properties are in Table 7-3. Finally, the channel head material is SA-216 Grade WCC, and its properties are in Table 7-4. The material properties are from Reference 26, and match the properties listed in the ASME Code.

The perforated tubesheet in the Model F channel head assembly is treated as an equivalent solid plate in the global finite element analysis. An accurate model of the overall plate behavior was achieved by using the concept of an equivalent elastic material with anisotropic properties. For square tubesheet hole patterns, the equivalent material properties depend on the orientation of loading with respect to the symmetry axes of the pattern. An accurate approximation was developed (Reference 27), where energy principles were used to derive effective average isotropic elasticity matrix coefficients for the in-plane loading. The average isotropic stiffness formulation gives results that are consistent with those using the Minimum Potential Energy Theorem, and the elasticity problem thus becomes axisymmetric. The solution for strains is sufficiently accurate for design purposes, except in the case of very small ligament efficiencies, which are not of issue for the evaluation of the SG tubesheet.

The stress-strain relations for the axisymmetric perforated part of the tubesheet are given by:

$$\begin{bmatrix} \sigma_R^* \\ \sigma_\theta^* \\ \sigma_Z^* \\ \tau_{RZ}^* \end{bmatrix} = \begin{bmatrix} D_{11} & D_{12} & D_{13} & 0 \\ D_{21} & D_{22} & D_{23} & 0 \\ D_{31} & D_{32} & D_{33} & 0 \\ 0 & 0 & 0 & D_{44} \end{bmatrix} \begin{bmatrix} \varepsilon_R^* \\ \varepsilon_\theta^* \\ \varepsilon_Z^* \\ \gamma_{RZ}^* \end{bmatrix}$$

where the elasticity coefficients are calculated as:

$$D_{11} = D_{22} = \frac{\overline{E}_p^*}{f(1+\overline{v}_p^*)}\left[1 - \frac{\overline{E}_p^*}{E_Z^*}v^2\right] + \frac{1}{2}\left[\overline{G}_p^* - \frac{\overline{E}_p^*}{2(1+\overline{v}_p^*)}\right]$$

$$D_{21} = D_{12} = \frac{\overline{E}_p^*}{f(1+\overline{v}_p^*)}\left[\overline{v}_p^* + \frac{\overline{E}_p^*}{E_Z^*}v^2\right] - \frac{1}{2}\left[\overline{G}_p^* - \frac{\overline{E}_p^*}{2(1+\overline{v}_p^*)}\right]$$

$$D_{13} = D_{23} = D_{31} = D_{32} = \frac{\overline{E}_p^* v}{f}$$

$$D_{33} = \frac{E_Z^*(1-\overline{v}_p^*)}{f} \text{ and } D_{44} = \overline{G}_Z^*$$

$$\text{where } f = 1 - \overline{v}_p^* - 2\frac{\overline{E}_p^*}{E_Z^*}v^2 \text{ and } \overline{G}_p^* = \frac{\overline{E}_d^*}{2(1+\overline{v}_d^*)}.$$

Here,

$\overline{E}_p^*$ = Effective elastic modulus for in-plane loading in the pitch direction,
E_z^* = Effective elastic modulus for loading in the thickness direction,
$\overline{v}_p^*$ = Effective Poisson's ratio for in-plane loading in the pitch direction,
$\overline{G}_p^*$ = Effective shear modulus for in-plane loading in the pitch direction,
$\overline{G}_z^*$ = Effective modulus for transverse shear loading,
$\overline{E}_d^*$ = Effective elastic modulus for in-plane loading in the diagonal direction,
$\overline{v}_d^*$ = Effective Poisson's ratio for in-plane loading in the diagonal direction, and,
v = Poisson's ratio for the solid material.

The tubesheet is a thick plate and the application of the pressure load results in a generalized plane strain condition. The pitch of the square, perforated hole pattern is 0.98 inches and nominal hole diameters are 0.703 inch. The ID of the tube after expansion into the tubesheet is taken to be 0.625 inch based on an assumption of 1% thinning during installation. Equivalent properties of the tubesheet are calculated without taking credit for the stiffening effect of the tubes.

$$\text{Ligament Efficiency, } \eta = \frac{h_{nominal}}{P_{nominal}}$$

where: $h_{nominal}$ = $P_{nominal}$ - $d_{maximum}$
$P_{nominal}$ = 0.980 inches, the pitch of the square hole pattern
$d_{maximum}$ = .706 inches, the tube hole diameter

Therefore, $h_{nominal}$ = 0.2745 inches (0.980-0.706), and η = 0.2796 when the tubes are not included. From Slot, Reference 28, the in-plane mechanical properties for Poisson's ratio of 0.3 are:

Property	Value
E*p/E	0.3977
v*p	0.1630
G*p/G	0.1627
E*z/E	0.5890
G*z/G	0.4137

where the subscripts p and d refer to the pitch and diagonal directions, respectively. These values are substituted into the expressions for the anisotropic elasticity coefficients given previously. In the global model, the X-axis corresponds to the radial direction, the Y-axis to the vertical or tubesheet thickness direction, and the Z-axis to the hoop direction. The directions assumed in the derivation of the elasticity coefficients were X- and Y-axes in the plane of the tubesheet and the Z-axis through the thickness. In addition, the order of the stress components in the WECAN/Plus (Reference 28) elements used for the global model is σ_{xx}, σ_{yy}, τ_{xy}, and σ_{zz}. The mapping between the Reference 27 equations and WECAN/+ is therefore:

Coordinate Mapping	
Reference 27	WECAN/+
1	1
2	4
3	2
4	3

Table 7-2 gives the modulus of elasticity, E, of the tubesheet material at various temperatures. Using the equivalent property ratios calculated above in the equations presented at the beginning of this section gives the elasticity coefficients for the equivalent solid plate model in the perforated region of the tubesheet. These elasticity coefficients are listed in Table 7-5 for the tubesheet, without accounting for the effect of the tubes. The values for 600°F were used for the finite element unit load runs. The material properties of the tubes are not utilized in the finite element model, but are listed in Table 7-1 for use in the calculations of the tube/tubesheet contact pressures.

7.1.2 Finite Element Model

The analysis of the contact pressures utilizes conventional (thick shell equations) and finite element analysis techniques. A finite element model was developed for the Model F SG channel head/tubesheet/shell region (which includes the Salem Unit 1 steam generators) in order to determine the tubesheet rotations. The elements used for the models of the channel head/tubesheet/shell region were the quadratic version of the 2-D axisymmetric isoparametric elements STIF53 and STIF56 of WECAN/Plus (Reference 28). The model for the Model F steam generator is shown in Figure 7-2.

The unit loads applied to this model are listed below:

Unit Load	Magnitude
Primary Side Pressure	1000 psi
Secondary Side Pressure	1000 psi
Tubesheet Thermal Expansion	500°F
Shell Thermal Expansion	500°F
Channel Head Thermal Expansion	500°F

The three temperature loadings consist of applying a uniform thermal expansion to each of the three component members, one at a time, while the other two remain at ambient conditions. The boundary conditions imposed for all five cases are: UX=0 at all nodes on the centerline, and UY=0 at one node on the lower surface of the tubesheet support ring. In addition, an end cap load is applied to the top of the secondary side shell for the secondary side pressure unit load equal to:

$$P_{endcap} = -\left[\frac{(R_i)^2}{(R_o)^2 - (R_i)^2}\right] P = -7742.5 \text{ psi}^1$$

[1]Negative sign denotes direction of load in WECAN/PLUS

where, R_i = Inside radius of secondary shell in finite element model = 64.69 in.
R_o = Outside radius of secondary shell in finite element model = 68 in.
P = Secondary pressure unit load = 1000 psi.

This yielded displacements throughout the tubesheet for the unit loads.

7.1.3 Tubesheet Rotation Effects

Loads are imposed on the tube as a result of tubesheet rotations under pressure and temperature conditions. Previous calculations performed showed that the displacements at the center of the tubesheet when the divider plate is included are [

]a,c,e

The radial deflection at any point within the tubesheet is found by scaling and combining the unit load radial deflections at that location according to:

a,c,e

[ ]

This expression is used to determine the radial deflections along a line of nodes at a constant axial elevation (e.g. top of the tubesheet) within the perforated area of the tubesheet. The expansion of a hole of diameter D in the tubesheet at a radius R is given by:

a,c,e

[ ]

UR is available directly from the finite element results. dUR/dR may be obtained by numerical differentiation.

The maximum expansion of a hole in the tubesheet is in either the radial or circumferential direction. [

:

]a,c,e

Where SF is a scale factor between zero and one. For the eccentricities typically encountered during tubesheet rotations, [ ]a,c,e These values are listed in the following table:

a,c,e

The data were fit to the following polynomial equation:

[ ]a,c,e

The hole expansion calculation as determined from the finite element results includes the effects of tubesheet rotations and deformations caused by the system pressures and temperatures. It does not include the local effects produced by the interactions between the tube and tubesheet hole. Standard thick shell equations, including accountability for the end cap axial loads in the tube (Reference 30), in combination with the hole expansions from above are used to calculate the contact pressures between the tube and the tubesheet.

The unrestrained radial expansion of the tube OD due to thermal expansion is calculated as:

$$\Delta R_t^{th} = c\,\alpha_t\,(T_t - 70)$$

and from pressure acting on the inside and outside of the tube as,

$$\Delta R_{to}^{pr} = \frac{P_i c}{E_t}\left[\frac{(2-\nu)b^2}{c^2 - b^2}\right] - \frac{P_o c}{E_t}\left[\frac{(1-2\nu)c^2 + (1+\nu)b^2}{c^2 - b^2}\right],$$

where: P_i = Internal primary side pressure, P_{pri} psi
P_o = External secondary side pressure, P_{sec} psi
b = Inside radius of tube = 0.3119 in.
c = Outside radius of tube = 0.3515 in.
α_t = Coefficient of thermal expansion of tube, in/in/°F
E_t = Modulus of Elasticity of tube, psi
T_t = Temperature of tube, °F ,and,
ν = Poisson's Ratio of the material.

The thermal expansion of the hole ID is included in the finite element results and does not have to be expressly considered in the algebra, however, the expansion of the hole ID produced by pressure is given by:

$$\Delta R_{TS}^{pr} = \frac{P_i c}{E_{TS}}\left[\frac{d^2 + c^2}{d^2 - c^2} + \nu\right],$$

where: E_{TS} = Modulus of Elasticity of tubesheet, psi
d = Outside radius of cylinder which provides the same radial stiffness as the tubesheet, that is, [ ]^{a,c,e}

If the unrestrained expansion of the tube OD is greater than the expansion of the tubesheet hole, then the tube and the tubesheet are in contact. The inward radial displacement of the outside surface of the tube produced by the contact pressure is given by: (Note: The use of the term δ in this section is unrelated its potential use elsewhere in this report.)

$$\delta_t = \frac{P_2 c}{E_t}\left[\frac{c^2 + b^2}{c^2 - b^2} - \nu\right]$$

The radial displacement of the inside surface of the tubesheet hole produced by the contact pressure between the tube and hole is given by:

$$\delta_{TS} = \frac{P_2 c}{E_{TS}}\left[\frac{d^2 + c^2}{d^2 - c^2} + \nu\right]$$

The equation for the contact pressure P_2 is obtained from:

$$\delta_{to} + \delta_{TS} = \Delta R_{to} - \Delta R_{TS} - \Delta R_{ROT}$$

where ΔR_{ROT} is the hole expansion produced by tubesheet rotations obtained from finite element results. The ΔR's are:

$$\Delta R_{to} = c\alpha_t(T_t - 70) + \frac{P_{pri}c}{E_t}\left[\frac{(2-\nu)b^2}{c^2 - b^2}\right] - \frac{P_{sec}c}{E_t}\left[\frac{(1-2\nu)c^2 + (1+\nu)b^2}{c^2 - b^2}\right]$$

$$\Delta R_{TS} = \frac{P_{sec}c}{E_{TS}}\left[\frac{d^2 + c^2}{d^2 - c^2} + \nu\right]$$

The resulting equation is:

a,c,e

[ ]

For a given set of primary and secondary side pressures and temperatures, the above equation is solved for selected elevations in the tubesheet to obtain the contact pressures between the tube and tubesheet as a function of radius. The elevations selected ranged from the top to the bottom of the tubesheet. Negative "contact pressure" indicates a gap condition.

The OD of the tubesheet cylinder is equal to that of the cylindrical (simulate) collars (1.632 inches) designed to provide the same radial stiffness as the tubesheet, which was determined from a finite element analysis of a section of the tubesheet (References 31 and 20).

The tube inside and outside radii within the tubesheet are obtained by assuming a nominal diameter for the hole in the tubesheet (0.703 inch) and wall thinning in the tube equal to the average of that measured during hydraulic expansion tests. The final wall thickness is 0.0396 inch for the tube. The following table lists the values used in the equations above, with the material properties evaluated at 600°F. (Note that the properties in the following sections are evaluated at the primary fluid temperature).

Thick Cylinder Equations Parameter	Value
b, inside tube radius, in.	0.3119
c, outside tube radius, in.	0.3515
d, outside radius of cylinder w/ same radial stiffness as TS, in.	[ ]a,c,e
α_t, coefficient of thermal expansion of tube, in/in °F	$7.83 \cdot 10^{-6}$
E_t, modulus of elasticity of tube, psi	$28.7 \cdot 10^{6}$
α_{TS}, coefficient of thermal expansion of tubesheet, in/in °F	$7.42 \cdot 10^{-6}$
E_{TS}, modulus of elasticity of tubesheet, psi	$26.4 \cdot 10^{6}$

7.1.4 Salem Unit 1 Contact Pressures

7.1.4.1 Bounding Operating Conditions

The loadings considered in the analysis are based on an umbrella set of conditions as defined in Reference 32. The current operating parameters from Reference 17 are used. The temperatures and pressures for normal operating conditions at Salem Unit 1 are bracketed by the following case:

Loading	T_{max} (2)
Primary Pressure	2235 psig
Secondary Pressure	731 psig
Primary Fluid Temperature (T_{hot})	601.8°F
Secondary Fluid Temperature	510.2°F

The primary pressure [

]a,c,e

7.1.4.2 Faulted Conditions

Of the faulted conditions, steamline break (SLB) is the most limiting in the hot leg. Feedline break is the most limiting condition in the cold leg.

Previous analyses have shown that SLB is the limiting faulted condition, with tube lengths required to resist push out during a postulated loss of coolant accident (LOCA) typically less than one-fourth of the tube lengths required to resist pull out during SLB (References 30, 20 and 21). Therefore LOCA was not considered in this analysis.

7.1.4.3 Steam Line Break

As a result of SLB, the faulted SG will rapidly blow down to atmospheric pressure, resulting in a large ΔP across the tubes and tubesheet. The entire flow capacity of the auxiliary feedwater system would be delivered to the dry, hot shell side of the faulted SG. The primary side re-pressurizes to the pressurizer safety valve set pressure. The hot leg temperature decreases throughout the transient, reaching a minimum temperature of 420°F. The pertinent parameters are listed below. The combination of parameters yielding the most limiting results is used.

Primary Pressure	=	2560 psig
Secondary Pressure	=	0 psig
Primary Fluid Temperature (T_{hot})	=	420°F
Secondary Fluid Temperature	=	260°F

For this set of primary and secondary side pressures and temperatures, the equations derived in Section 7.2 below are solved for the selected elevations in the tubesheet to obtain the contact pressures between the tube and tubesheet as a function of tubesheet radius for the hot leg.

7.1.4.4 Summary of FEA Results for Tube-to-Tubesheet Contact Pressures

For Salem Unit 1, the contact pressures between the tube and tubesheet for various plant conditions are listed in Table 7-5 and plotted versus radius on Figure 7-3 through 7-7. The application of these values to the determination of the required engagement length is discussed in Section 7.2.

7.2 DETERMINATION OF REQUIRED ENGAGEMENT LENGTH OF THE TUBE IN THE TUBESHEET

The elimination of a portion of the tube (i.e., a portion of the pressure boundary) within the tubesheet from the in-service inspection requirement constitutes a change in the pressure boundary. The required length of engagement of the tube in the tubesheet to resist performance criteria tube end cap loads is designated by the variable H*. This length is based on structural requirements only and does not include any connotation associated with leak rate, except perhaps in a supporting role with regard to the leak rate expectations relative to normal operating conditions. The contact pressure is used for estimating the magnitude of the anchorage of the tube in the tubesheet over the H* length.

To take advantage of the tube-to-tubesheet joint anchorage, it is necessary to demonstrate that the [

]a,c,e

The end cap loads for Normal and Faulted conditions are:

Normal (maximum): $\pi \cdot (2235\text{-}731) \cdot (0.703)^2 /4 = 583.78$ lbs.
Faulted (SLB): $\pi \cdot 2560 \cdot (0.703)^2 /4 = 993.7$ lbs.

Seismic loads have also been considered, but they are not significant in the tube joint region of the tubes.

A key element in estimating the strength of the tube-to-tubesheet joint during operation or postulated accident conditions is the residual strength of the joint stemming from the expansion preload due to the manufacturing process, i.e., hydraulic expansion. During operation the preload increases because the thermal expansion of the tube is greater than that of the tubesheet and because a portion of the internal pressure in the tube is transmitted to the interface between the tube and the tubesheet. However, the tubesheet bows upward leading to a dilation of the tubesheet holes at the top of the tubesheet and a contraction at the bottom of the tubesheet when the primary-to-secondary pressure difference is positive.

The dilation of the holes acts to reduce the contact pressure between the tubes and the tubesheet. The H* lengths are based on the pullout resistance associated with the net contact pressure during normal or accident conditions. The calculation of the residual strength involves a conservative approximation that the strength is uniformly distributed along the entire length of the tube. This leads to a lower bound estimate of the strength and relegates the contribution of the preload to having a second order effect on the determination of H*.

For the partial-length RPC evaluation, tube-to-tubesheet contact pressure was calculated [

]a,c,e

The force resisting pullout acting on a length of a tube between elevations h_1 and h_2 is given by:

$$F_l = (h_2 - h_1)F_{HE} + \mu\pi d \int_{h_1}^{h_2} P dh$$

where: F_{HE} = Resistance to pull out due to the initial hydraulic expansion = 522.3 lb/inch,
P = Contact pressure acting over the incremental length segment dh, and,
μ_f = Coefficient of friction between the tube and tubesheet, conservatively assumed to be 0.2 for the pullout analysis to determine H*.

The contact pressure is assumed to vary linearly between adjacent elevations in the top part of Tables 7-7 through 7-11, so that between elevations L1 and L2,

$$P = P_1 + \frac{(P_2 - P_1)}{(L_2 - L_1)}(h - L_1)$$

or,

a,c,e

[ ]

so that,

a,c,e

[ ]

where u_f is the coefficient of friction. This equation was used to accumulate the force resisting pullout from the top of the tubesheet to each of the elevations listed in the lower parts of Tables 7-7 through 7-11. The above equation is also used to find the minimum contact lengths needed to meet the pullout force requirements. In Zone D, the length calculated was 7.05 inches for the necessary performance criterion which corresponds to a cumulative pullout force of 1693.11 lbf in the cold leg.

The top part of Table 7-9 lists the contact pressures through the thickness at each of the radial sections for Faulted (SLB) condition. The last row, "h(0)," of this part of the table lists the maximum tubesheet elevation at which the contact pressure is greater than or equal to zero. The above equation is used to calculate the force resisting pull out from the top of the tubesheet for each of the elevations listed in the lower part of Table 7-9. In Zone D, this length is 6.62 inches for the 1.4 times normal the accident pressure performance criterion which corresponds to a pullout force of 1391.13 lbs in the hot leg for the Faulted (SLB) condition. The H* calculations for each loading condition at each of the radii considered are summarized in Reference 26. The H* results for each zone are summarized in Table 7-13.

The bounding condition for the determination of the H* length is the FLB performance criterion for Zones A, B, and C (cold leg). The minimum contact length for the FLB condition is 7.05 inches in Zone D. In Zone C, the minimum contact length is 6.95 inches. In Zone B, the minimum contact length is calculated to be 4.66 inches. For Zone A, the bounding condition for determining the H* length is NO conditions. The minimum contact length is calculated to be 2.25 inches.

7.3 NDE UNCERTAINTY DISCUSSION

Application of alternate repair criteria for axial and circumferential cracks requires that a provision be made in the engineering evaluations for the uncertainties inherent in the measurement of NDE parameters. For H* these include the distance between the BET and the uppermost crack tip and the H* length. The following uncertainties are considered in the determination of the proposed inspection length and are based on a study conducted for determining similar parameters in the development of a W* criteria for several other plants (Reference 3):

Measurement	Plus Point Probe (300 kHz) Uncertainty (Inches)	
	Measurement Uncertainty	Applied Error
BET- Flaw Tip	0.00 ± 0.17	0.28
H* Length	-0.11 ± 0.14	0.12

The BET-to-crack-tip uncertainty is used to decrease the distance between the BET and the upper most crack tip below the BET. The applied error value of 0.28 inch assures a one-sided 95% upper bound on the actual distance and is the net sum of the average error plus 1.65 times the standard deviation.

The H* length uncertainty of 0.12 inch is applied directly to the H* value.

The H*/B* inspection length is referenced to the top of the tubesheet versus the bottom of the expansion transition as is the case for W*. The maximum uncertainty for the location of the bottom of the hydraulic expansion transition (BET) to the top of the tubesheet assumed in the H*/B* report is 0.3 inch and is based on the following discussion:

The original manufacturing engineering specification for the distance from the top of the tubesheet to the bottom of the expansion transition, referred to as the crevice depth, was a maximum acceptable length of 0.25 inch. Per LTR-SGDA-03-129, it is stated that this specification remained in effect throughout the manufacture of SGs with hydraulically expanded tube-to-tubesheet joints. The process specification employed in the manufacturing facilities identified target values of 0.060, 0.090, and 0.125 inches, depending on the time of manufacture, with the difference between the fabrication and engineering numbers constituting a buffer range for expansions greater than the manufacturing allowance. For example, for the Callaway SGs, the manufacturing requirement for the crevice depth was 0.050 inches.

The results of a total of 1600 measurements of hydraulic joint crevice depths for units made after the Callaway SGs were fabricated and after a fabrication process change was implemented that was intended to increase the crevice depth to decrease the potential for bulges above the top of the tubesheet follow. The average depth of the crevices was measured to be 0.145 inches with a standard deviation of 35 mils. The likelihood of a crevice depth greater than 300 mils was calculated to be 0.0006.

Measurements taken again after changes were made to reduce the average crevice depth is likely more representative of the crevice depths in most Model SGs with hydraulically expanded tubes in the tubesheet. The average depth is on the order of 99 mils with a standard deviation of 25 mils. The likelihood of fabricating a crevice at random with a depth greater than 150 mils is calculated to be 0.02. The likelihood of exceeding a depth of 300 mils is calculated to be less than 0.0001.

In either case described above, it is justified to use a crevice depth of 0.3 inch to the bottom of the BET (Reference 54).

7.4 DIVIDER PLATE CRACKING ISSUE

Indications of cracks in the divider plates have been reported in several steam generators located in France. These indications have been observed in steam generators located at the Chinon, Saint-Laurent, Dampierre and Gravelines nuclear power stations. The cracks were observed on the hot leg side of the divider plate in the stub runner divider plate weld, stub runner base metal and also at or in the divider plate itself. See Figure 7-8. The network of cracks has been reported to extend along most of the divider plate (~6 feet) and have also been reported to be relatively shallow with depth, typically less than 2 mm (~75 mils deep).

Since these steam generators used an Alloy 600 material in the divider plate to stub runner weld, the French utilities decided to inspect this location to determine if any indications of cracking could be found during a visual inspection. During the initial visual inspection it was reported that indications of cracks were observed but that they appeared to be shallow in depth. Various other methods were used in subsequent refueling outages to determine the extent of cracking and to determine if the cracks were growing at an excessive rate. Available information indicates that these inspections have been performed since 1993 using a combination of liquid penetrant examination (PT) and visual examination (VT) methods with indications of cracking observed in some of these plants. Through the winter of 2005, a total of thirty five inspections using VT and PT were performed in the French 900 megawatt (MW) and 1300 MW units with indications of cracking being found in at least four of the plants as noted above. As a result of these findings, additional inspection capability, including ultrasonic testing (UT) has been developed by Westinghouse in Europe to assist in the inspections.

Primary water stress corrosion cracking (PWSCC) is a know mechanism of cracking in Alloy 600 and it is likely this is the primary contributor to cracking at this location. However, other potential contributors to cracking have been reported to be defects in the weld or base material, along with deformations associated with loose part impingement.

The maximum depth of the cracks observed in the French units has been reported to be about 2 mm (~75 mils). There has been a succession of inspection methods (VT, PT, and then UT) used in plants that have had indications of cracking. However, it has been reported that consecutive inspections using identical methods have not been performed to date. Therefore, it is not possible to develop an accurate growth rate from the French inspections. From the available information it can be inferred that the cycle-to-cycle growth rate of the cracks is small based on the following: The difficulty in obtaining an accurate measure of the depth of the crack due to the shallowness of the crack (smaller cracks are harder to detect than larger cracks), the continued reports of finding only shallow depth cracks, and the relatively long period of time that these cracks have been known to exist.

Since the cracks included by the French experience are small with relatively small cycle-to-cycle growth rates, their effect on the divider plate function is also expected to be small. It would be expected that cracks of the size reported would not affect the general displacement response of the tubesheet since only a modest change in divider plate stiffness would be expected. In addition, it would not be expected that cracks of the size reported would rapidly grow due to mechanically induced loadings resulting from normal/upset events or during a faulted event. However, there may be a potential for long term growth of these cracks which could eventually affect tubesheet displacements and result in an increased rate of crack propagation.

Westinghouse is currently performing analysis to determine the effect of these types of cracks in the divider plate on longer term operation. The scope of work includes determining the consequences of relatively large (but not totally through-wall) cracks in the divider plate. Through-wall cracks are not likely to rapidly occur based on the French experience. The analysis will consider the effect of increased tubesheet displacements and the resulting stress near the crack tip which may propagate the crack due to mechanical methods.

Tubesheet displacements can directly affect multiple regions in the SG that include such areas as:

a. Tubesheet stress

b. Secondary side shell stress

c. Channel head stress

d. Tube stress

e. Plug retention/acceptability issues.

Should divider plate cracking significantly change tubesheet displacements, then there would be a potential to affect calculations performed to support various ARCs such as those performed in support of H*, B* or W*. However, as indicated above, for the type of cracks found in the French units, i.e. small depths with apparent low growth rates, it would not be expected that these cracks would affect the current results associated with the ARCs. In the interim, a bounding analysis has been performed assuming no constraint on tubesheet deflection is provided by the divider plate. The bounding value for H* and B* is determined to be 12 inches (Reference 56).

In summary, should cracks of the form found in the French units also be found in Westinghouse model steam generators, it would be reasonable to conclude that rapid crack growth would not be expected. In addition, any changes to ARCs resulting from any corresponding increased tubesheet displacement would also be expected to be minimal. However, the effects of long term operation with cracks in the divider plate needs to be evaluated to determine if the cracks could grow to a point where either rapid crack growth could occur during operation of the SG or if increased tubesheet displacements could affect other aspects of the steam generator, such as tubesheet stress, secondary side shell stress, channel head stress, tube stress, plug retention/acceptability issues and the ARCs (Reference 55).

Table 7-1. Summary of Material Properties Alloy 600 Tube Material							
Property	Temperature (°F)						
	70	200	300	400	500	600	700
Young's Modulus ($psi \cdot 10^6$)	31.00	30.20	29.90	29.50	29.00	28.70	28.20
Thermal Expansion ($in/in/°F \cdot 10^{-6}$)	6.90	7.20	7.40	7.57	7.70	7.82	7.94
Density ($lb\text{-}sec^2/in^4 \cdot 10^{-4}$)	7.94	7.92	7.90	7.89	7.87	7.85	7.83
Thermal Conductivity ($Btu/sec\text{-}in\text{-}°F \cdot 10^{-4}$)	2.01	2.11	2.22	2.34	2.45	2.57	2.68
Specific Heat ($Btu\text{-}in/lb\text{-}sec^2\text{-}°F$)	41.2	42.6	43.9	44.9	45.6	47.0	47.9

Table 7-2. Summary of Material Properties for SA-508 Class 2a Tubesheet Material							
Property	Temperature (°F)						
	70	200	300	400	500	600	700
Young's Modulus ($psi \cdot 10^6$)	29.20	28.50	28.00	27.40	27.00	26.40	25.30
Thermal Expansion ($in/in/°F \cdot 10^{-6}$)	6.50	6.67	6.87	7.07	7.25	7.42	7.59
Density ($lb\text{-}sec^2/in^4 \cdot 10^{-4}$)	7.32	7.30	7.29	7.27	7.26	7.24	7.22
Thermal Conductivity ($Btu/sec\text{-}in\text{-}°F \cdot 10^{-4}$)	5.49	5.56	5.53	5.46	5.35	5.19	5.02
Specific Heat ($Btu\text{-}in/lb\text{-}sec^2\text{-}°F$)	41.9	44.5	46.8	48.8	50.8	52.8	55.1

Table 7-3. Summary of Material Properties SA-533 Grade A Class 2 Shell Material							
Property	Temperature (°F)						
	70	200	300	400	500	600	700
Young's Modulus ($psi \cdot 10^6$)	29.20	28.50	28.00	27.40	27.00	26.40	25.30
Thermal Expansion ($in/in/°F \cdot 10^{-6}$)	7.06	7.25	7.43	7.58	7.70	7.83	7.94
Density ($lb\text{-}sec^2/in^4 \cdot 10^{-4}$)	7.32	7.30	7.283	7.265	7.248	7.23	7.211

Table 7-4. Summary of Material Properties SA-216 Grade WCC Channelhead Material							
Property	Temperature (°F)						
	70	200	300	400	500	600	700
Young's Modulus ($psi \cdot 10^6$)	29.50	28.80	28.30	27.70	27.30	26.70	25.50
Thermal Expansion ($in/in/°F \cdot 10^{-6}$)	5.53	5.89	6.26	6.61	6.91	7.17	7.41
Density ($lb\text{-}sec^2/in^4 \cdot 10^{-4}$)	7.32	7.30	7.29	7.27	7.26	7.24	7.22

Table 7-5. Equivalent Solid Plate Elasticity Coefficients for 44F Perforated TS SA-508 Class 2a Tubesheet Material (psi) a,c,e

Table 7-6. Tube/Tubesheet Maximum & Minimum Contact Pressures and H* Depths for Salem Unit 1 Steam Generators (Hot Leg and Cold Leg)

a,c,e

Notes:

a. Contact pressures values do not include the residual from the tube installation.

b. All H* values include a 0.3 inch bounding allowance for the elevation of the BET relative to the TTS

Table 7-7. Cumulative Forces Resisting Pull Out from the TTS Salem Unit 1
Hot Leg Normal Conditions – T_{hot} = 601.8°F P_{sec} = 731 psig

a,c,e

Table 7-8. Cumulative Forces Resisting Pull Out from the TTS Salem Unit 1
Hot Leg Normal Conditions – T_{hot} = 613.1°F, P_{sec} = 807 psig

a,c,e

Table 7-9. Cumulative Forces Resisting Pull Out from the TTS Salem Unit 1 Faulted (SLB) Conditions, Psec = 0 psig (Hot and Cold Leg)

a,c,e

Table 7-10. Cumulative Forces Resisting Pull Out from the TTS Salem Unit 1 FLB Conditions, T_{hot} = 601.8°F

a,c,e

Table 7-11. Cumulative Forces Resisting Pull Out from the TTS Salem Unit 1 FLB Conditions, T_{hot} = 613.1°F

a,c,e

Table 7-12. Summary of H* Calculations for Salem Unit 1 (Hot Leg Only)

a,c,e

Table 7-13. H* Summary Table
Structural Criteria Required Engagement[5]

Zone	Limiting Loading Condition	Engagement from TTS (inches)	
		Hot Leg	Cold Leg
A	$3 \cdot \Delta P_{NOp}$ (1,2)	2.20[3)]	2.25(3, 4)
B	$1.4 \cdot \Delta P_{SLB}$ (1,2)	4.26 (3)	4.66 (3,6)
C	$1.4 \cdot \Delta P_{SLB}$ (1,2)	6.53 (3)	6.95 (3,6)
D	$1.4 \cdot \Delta P_{SLB}$ (1,2)	6.62 (3)	7.05 (3,6)

Notes:

1. Seismic loads have been considered and are not significant in the tube joint region (Reference 25).
2. The scenario of tubes locked at support plates is not considered to be a credible event in Model F SGs as they are manufactured with stainless steel support plates. However, conservatively assuming that the tubes become locked at 100% power conditions, the maximum force induced in an active tube as the SG cools to room temperature is [ ]a,c,e
3. 0.3 inches has been added to the H* values to account for the BET location relative to the TTS.
4. $1.4 \cdot \Delta P_{FLB}$ conditions.
5. B* requirements for leak rate are provided in Table 9-2.
6. The maximum H* Value occurs at FLB condition, $T_{hot} = 601.8°F$

a,c,e

Figure 7-1. Definition of H* Zones.
(Zone D is the most inboard and Zone A the most outboard.)

a,c,e

Figure 7-2. Finite Element Model of Model F Tubesheet Region

a.c.e

Figure 7-3. Contact Pressures for Normal Condition (Hot Leg) at Salem Unit 1

a.c.e

Figure 7-4. Contact Pressures for Normal Condition (High T_{avg}) at Salem Unit 1

a.c.e

Figure 7-5. Contact Pressures for SLB Faulted Condition at Salem Unit 1

a,c,e

Figure 7-6. Tube/Tubesheet Contact Pressures FLB Faulted Conditions – Salem Unit 1 Hot Leg Low T_{avg}

a,c,e

Figure 7-7. Tube/Tubesheet Contact Pressures FLB Condition – Salem Unit 1 Hot Leg, High T_{avg}

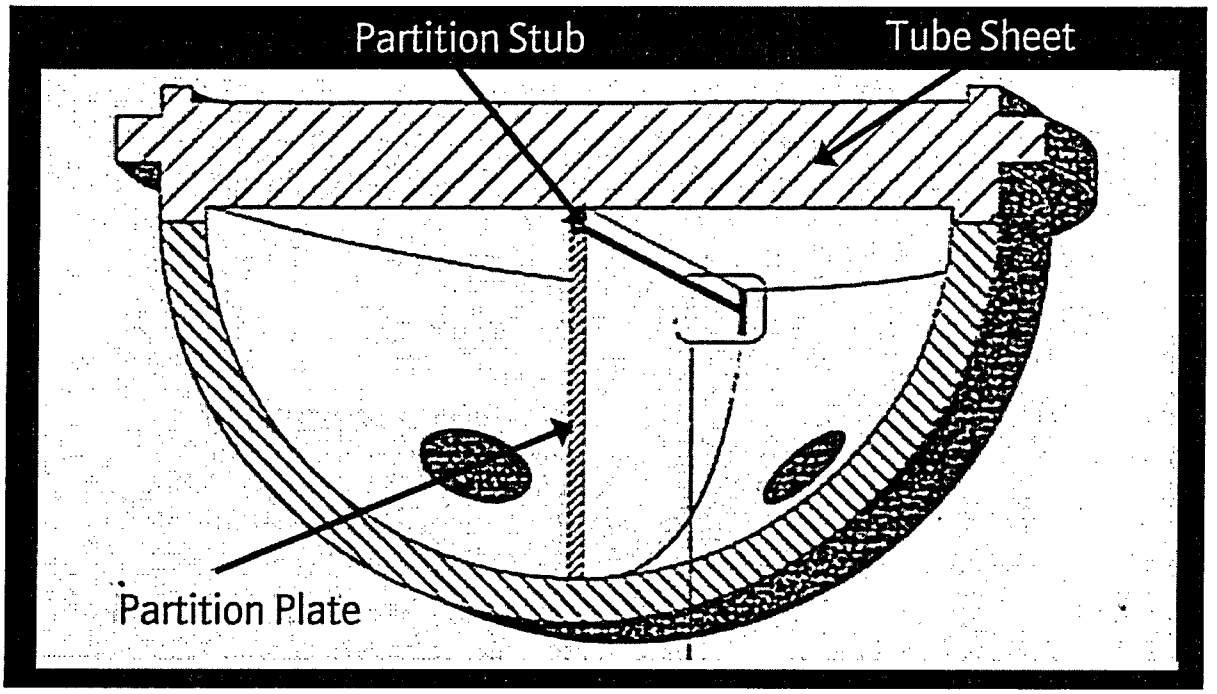

Figure 7-8. Location of Cracking Observed Along Partition Stub in EDF SGs

8.0 LEAK RATE ANALYSIS OF CRACKED TUBE JOINTS

This section of the report presents a discussion of the leak rate expectations from axial and circumferential cracking confined to the tube-to-tubesheet joint region, including the tack expansion region, the tube-to-tubesheet welds and areas where degradation could potentially occur due to bulges and overexpansions within the tube at a distance at or below 8 inches from the top of the tubesheet. It is noted that the methods discussed below support a permanent change to the Salem Unit 1 Technical Specifications. With regard to the inherent conservatism embodied in the application of any predictive methods it is noted that the presence of cracking was not confirmed because removal of a tube section was not performed at Catawba 2 or Vogtle 1.

8.1 THE BELLWETHER PRINCIPLE FOR NORMAL OPERATION TO STEAM LINE BREAK LEAK RATES

From an engineering expectation standpoint, if there is no significant primary-to-secondary leakage during normal operation, there should likewise be no significant leakage during postulated accident conditions from indications located approximately below the mid-plane of the tubesheet. The rationale for this is based on consideration of the deflection of the tubesheet with attendant dilation and diminution (expansion and contraction) of the tubesheet holes. In effect, the leakage flow area depends on the contact pressure between the tube and tubesheet and would be expected to decrease during postulated accident conditions below some distance from the top of the tubesheet. The primary-to-secondary pressure difference during normal operation is on the order of 1200 to 1500 psid, while that during a postulated accident, e.g., steam line and feed line break, is on the order of 2560 to 2650 psid.[1] Above the neutral plane of the tubesheet the tube holes tend to experience a dilation due to pressure induced bow of the tubesheet. This means that the contact pressure between the tubes and the tubesheet would diminish above the neutral plane in the central region of the tubesheet at the same time as the driving potential would increase. Therefore, if there was leakage through the tube-to-tubesheet crevice during normal operation from a through-wall tube indication, that leak rate could be expected to increase during postulated accident conditions. Based on early NRC staff queries regarding the leak rate modeling code associated with calculating the expected leak rate, see Reference 33 for example, it was expected that efforts to license criteria based on estimating the actual leak rate as a function of the contact pressure during faulted conditions on a generic basis would be problematic.

As noted, the tube holes diminish in size below the neutral plane of the tubesheet because of the upward bending and the contact pressure between the tube and the tubesheet increases. When the differential pressure increases during a postulated faulted event the increased bow of the tubesheet leads to an increase in the tube-to-tubesheet contact pressure, increasing the resistance to flow. Thus, while the dilation of the tube holes above the neutral plane of the tubesheet presents additional analytic problems in estimating the leak rate for indications above the neutral plane, the diminution of the holes below the neutral plane presents definitive statements to be made with regard to the trend of the leak rate, hence, the bellwether principle. Independent consideration of the effect of the tube-to-tubesheet contact pressure leads to similar conclusions with regard to the opening area of the cracks in the tubes, thus further restricting the leak rate beyond that through the interface between the tube and the tubesheet.

[1]The differential pressure could be on the order of 2405 psid if it is demonstrated that the power operated relief valves will be functional.

In order to accept the concept of normal operation being a bellwether for the postulated accident leak rate for indications above the neutral plane of the tubesheet, the change in leak rate had to be quantified using a somewhat complex, physically sound model of the thermal-hydraulics of the leak rate phenomenon. This is not necessarily the case for cracks considered to be present below the neutral plane of the tubesheet. This is because a diminution of the holes takes place during postulated accident conditions below the neutral plane relative to normal operation. For example, at a radius of 30.19 inches from the center of the SG, the contact pressure during normal operation at the bottom of the tubesheet is calculated to be 2759.66 to 2886.85 psi[2] (see the last contact pressure entry in the 30.19 inch radius columns of Tables 7-7 and 7-8, respectively), while the contact pressure during a postulated steam line break would be on the order of 4501.46 psi at the bottom of the tubesheet at a radius of 30.19 inches, see Table 7-9. (Note: The radii specified in the heading of the tables are the maximum values for the respective zones analyzed, hence the contact pressures in the center column correspond to the radius specified for the left column, etc. The leftmost column lists the contact pressure values for a radius of 4.016 inches.) The analytical model for the flow through the crevice, the Darcy equation for flow through porous media, indicates that flow would be expected to be proportional to the differential pressure. Thus, a doubling of the leak rate could be predicted if the change in contact pressure between the tube and the tubesheet were ignored. Examination of the nominal correlation in Reference 34 indicates that the resistance to flow (the loss coefficient) would increase during a postulated SLB event.

The leak rate from a crack located within the tubesheet is governed by the crack opening area, the resistance to flow through the crack, and the resistance to flow provided by the tube-to-tubesheet joint. The path through the tube-to-tubesheet joint is also frequently referred to as a crevice, but is not to be confused with the crevice left at the top of the tubesheet from the expansion process. The presence of the joint makes the flow from cracks within the tubesheet much different from the flow to be expected from cracks outside of the tubesheet. The tubesheet prevents outward deflection of the flanks of cracks, a more significant effect for axial than for circumferential cracks, which is a significant contributor to the opening area presented to the flow. In addition, the restriction provided by the tubesheet greatly restrains crack opening in the direction perpendicular to the flanks regardless of the orientation of the cracks. The net effect is a large, almost complete restriction of the leak rate when the tube cracks are within the tubesheet.

The leak path through the crack and the crevice is very tortuous. The flow must go through many turns within the crack in order to pass through the tube wall, even though the tube wall thickness is relatively small. The flow within the crevice must constantly change direction in order to follow a path that is formed between the points of hard contact between the tube and the tubesheet as a result of the differential thermal expansion and the internal pressure in the tube. There is both mechanical dispersion and molecular diffusion taking place. The net result is that the flow is best described as primary-to-secondary weepage. At its base, the expression used to predict the leak rate from tube cracks through the tube-to-tubesheet crevice is the Darcy expression for flow rate, Q, through porous media, i.e.,

$$Q = \frac{1}{K\mu}\frac{dP}{dz} \tag{1}$$

[2] The change occurs as a result of considering various hot and cold leg operating temperatures.

where μ is the viscosity of the fluid, P is the driving pressure, z is the physical dimension in the direction of the flow, and K is the "loss coefficient" which can also be termed the flow resistance if the other terms are taken together as the driving potential. The loss coefficient is found from a series of experimental tests involving the geometry of the particular tube-to-tubesheet crevice being analyzed, including factors such as surface finish, and then applied to the cracked tube situation.

The calculation of the leak rate during a postulated SLB is not based on a fluid temperature of 600° F but a temperature obtained from the examination of the equipment specification curves for the transient. The SLB transient is assumed to initiate while the plant is at hot standby conditions, i.e., 547° F. The primary side temperature drop is 127° F and the secondary side temperature drop is 287° F. An example of the temperature history during a postulated SLB for a Model F SG is shown on Figure 8-5. It can be readily seen that the most limiting differential pressure, 2560 psig, occurs when the temperature is at a relative steady state value of approximately 420° F). The H* engagement length required to resist SLB differential with a margin of 1.4 is longer later in the transient as the pressure differential increases and temperature remains relatively constant. Also, the B* length (discussed later in this report) to restrict the SLB leak rate to a factor of 2 relative to normal operation will also be longer for the SLB condition as the transient progresses.

If the leak rate during normal operation was 0.1 gpm (about 150 gpd), the postulated accident condition leak rate would be on the order of 0.2 gpm if only the change in differential pressure were considered, however, the estimate would be reduced when the increase in contact pressure between the tube and the tubesheet was included during a postulated steam line break event. An examination of the contact pressures as a function of depth in the tubesheet from the finite element analyses of the tubesheet as reported in Table 7-7 through Table 7-11 shows that the bellwether principle applies to a significant extent to all indications below the neutral plane of the tubesheet. At the neutral plane of the tubesheet, the increase in contact pressure shown on Figure 7-3 is more on the order of 42% relative to that during normal operation for all tubes regardless of radius. Still, the fact that the contact pressure increases means that the leak rate would be expected to be bounded by a factor of two relative to normal operation. At a depth of 16.9 inches from the top of the tubesheet the contact pressure increases by about 50% at a radius of 4.016 inches relative to that during normal operation. The flow resistance would be expected to increase by about 50%, thus the increase in driving pressure would be partially offset by the increase in the resistance of the joint.

The numerical results from the finite element analyses are presented on Figure 8-1 at the bottom of the tubesheet. A comparison of the contact pressure during postulated SLB conditions relative to that during NOp is also provided for depths of 16.9, 10.515 and 6.0 inches below the top of the tubesheet. The observations are discussed in the following.

- At the bottom of the tubesheet,
-
- Figure 8-1, the contact pressure increases by 1700 psi near the center of the tubesheet and exhibits no change at a radius of 55 inches.

- At 16.9 inches below the top of the tubesheet (a little over 4.1 inches from the bottom) the tubesheet the contact pressure increases by about 1200 psi at the center to a minimum of approximately 200 psid at a radius of 57 inches, Figure 8-2. The contact pressure during a SLB is everywhere greater than that during NOp. The influence of the channelhead and shell at the periphery causes the deformation to become non-uniform near the periphery.

- At 10.5 inches below the top of the tubesheet, (roughly the neutral plane), Figure 8-3 shows the contact pressure during SLB being uniformly greater than that during normal operation by about 400 psi.

- At a depth of 6.00 inches from the TTS, Figure 8-4, the contact pressure during a SLB stays constant at the center of the tubesheet and increases by about 500 psi near the periphery.

The absolute value of the contact pressure is not as important as the change in contact pressure because the parameter of interest in applying the B* criteria is the relative leak rate between NOp and SLB conditions. The analysis results indicate that there is an axial location within the tubesheet as a function of radius from the center where the contact pressure is invariant between NOp and SLB. The analysis results discussed in the next section include a plot of the invariant elevation for the Salem Unit 1 SGs (Figure 9-11). The distribution of the contact pressure would decrease near the TTS in the central region. Thus, it would not be sufficient to simply use an arbitrary depth value and suppose that the leak rate would be relatively unchanged even if the potential difference were the same without further analysis. However, the fact that the contact pressure generally increases below that elevation indicates that leak rate would be relatively unaffected a little deeper in the tubesheet.

8.2 LIGAMENT TEARING DISCUSSION

One of the concerns to address when dealing with cracks in SG tubes is the potential for ligament tearing. Ligament tearing may occur during a postulated accident when the differential pressure is significantly greater than during normal operation. The approach to dealing with the question is the same as that for circumferential cracks, that is, what is the ligament that will not tear during NOp conditions compared to the ligament that will tear during a postulated SLB event. The stress that is applied to the crack flanks during normal operation is the 2250 psi primary pressure. The stress during a SLB event is the 2560 psi pressure associated with the set point of the relief valves. The net difference is only 310 psi, hence the affect is expected to be small.

8.2.1 Circumferential Cracking

Ligament tearing considerations for circumferential tube cracks that are located below the H* depths within the tubesheet are significantly different from those for potential cracks at other locations. The reason for this is that H* has been determined using a factor of safety of three relative to the normal operating pressure differential and 1.4 relative to the most severe accident condition pressure differential. Therefore, the internal pressure end cap loads which normally lead to an axial stress in the tube are not transmitted below about 2/3 of the H* depth. This means that the only source of stress acting to extend the crack is the primary pressure acting on the flanks of the crack. Since the tube is captured within the tubesheet, there are additional forces acting to resist opening of the crack. The contact pressure between the tube and tubesheet results in a friction induced shear stress acting opposite to the direction of crack

opening, and the pressure on the flanks is compressive on the material adjacent to the plane of the crack, hence a Poisson's ratio radial expansion of the tube material in the immediate vicinity of the crack plane is induced which also acts to restrain the opening of the crack. In addition, the differential thermal expansion of the tube is greater than that of the carbon steel tubesheet, thereby inducing a compressive stress in the tube below the H* length.

A scoping evaluation of the [

]a,c,e

[

]a,c,e

In summary, considering the worst-case scenario, the likelihood of ligament tearing from radial circumferential cracks resulting from an accident pressure increase is small since at most, only 8% of the cross-sectional area is needed to maintain tube integrity. Also, since the crack face area will be less than the total cross-sectional area used above, the difference in the force applied as a result of normal operating and accident condition pressures will be less than the 35 lbs associated with the above numbers. Therefore, the potential for ligament tearing is considered to be a secondary effect of essentially negligible probability and should not affect the results and conclusions reported for the H* evaluation. The leak rate model does not include provisions for predicting ligament tearing and subsequent leakage, and increasing the complexity of the model to attempt to account for ligament tearing has been demonstrated to be not necessary (References 35 and 36).

8.2.2 Axial Cracking Discussion

The following evaluation considers the potential for ligament tearing of postulated axial cracks and to what extent such tearing would affect the technical basis for the LAR (Reference 35).

The tube area required to resist tearing due to an axially oriented crack can be calculated using traditional mechanics. The axial orientation of the damage in the tube means that the required area of the tube cross section to resist tearing and damage should be based on the local strength of the material around the crack. It is conservative, in this case, to [

]a,c,e

[a,c,e]

The results of the axial ligament tearing calculations detailed above are [

]a,c,e

Considering the worst-case scenario, the likelihood of ligament tearing from axial cracks resulting from an accident pressure increase is [

]a,c,e Therefore, the potential for axial ligament tearing is considered to be a secondary effect of essentially negligible probability and is not expected to affect the results and conclusions reported for

the B* evaluation. The leak rate model does not include provisions for predicting ligament tearing and subsequent leakage. Increasing the complexity of the model to attempt to account for axial or circumferential ligament tearing is not considered necessary.

a,c,e

Figure 8-1. Change in Contact Pressure at the Bottom of the Tubesheet

a,c,e

Figure 8-2. Change in Contact Pressure at 16.9 Inches Below the TTS

a,c,e

Figure 8-3. Change in Contact Pressure at 10.515 Inches Below the TTS

a,c,e

Figure 8-4. Change in Contact Pressure at 6.00 Inches Below the TTS

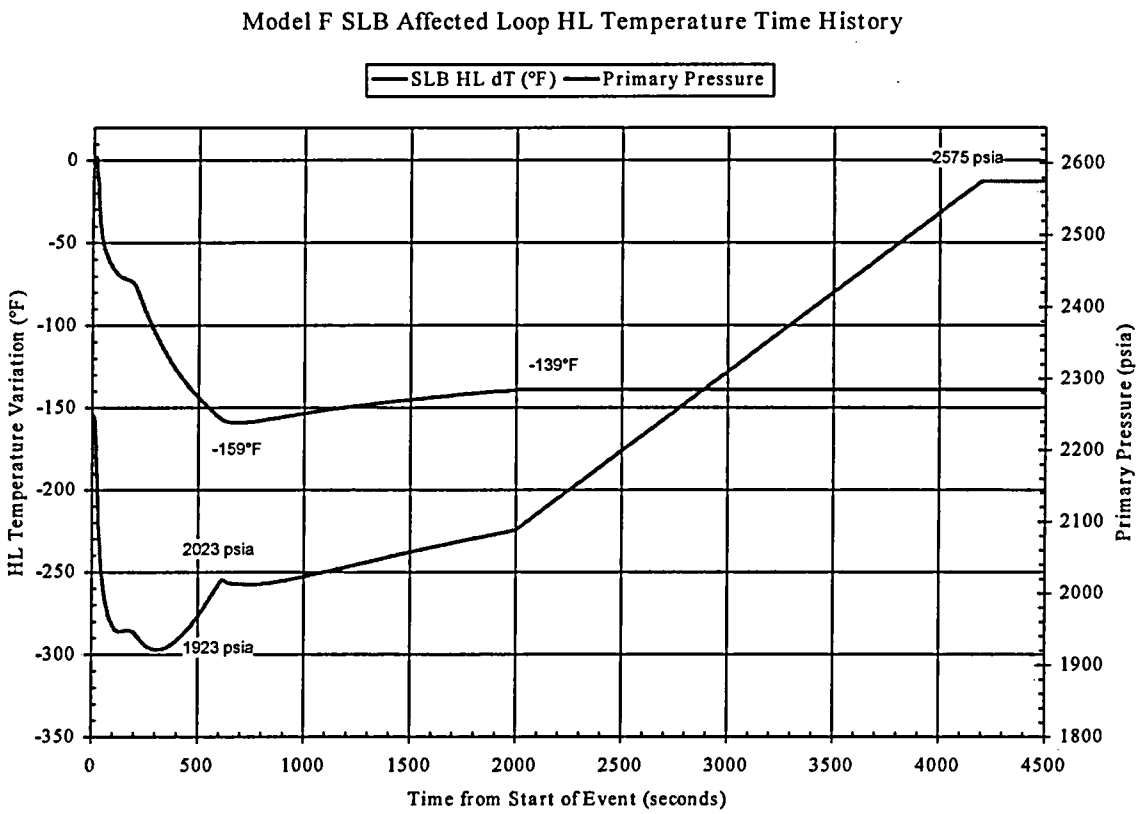

Figure 8-5. Model F SLB Affected Loop HL Temperature Time History

9.0 DETERMINATION OF THE B* DISTANCE

B* is the length of engagement in the tubesheet needed for the leak rate during a postulated steam line break (SLB) event to be bounded by a specified multiple of the leak rate during normal operation (NOp). See Figure 9-2. The rationale for the determination of B* is that there are changes during a SLB relative to NOp that lead to the expectation of an increase in the leak rate and other changes that lead to the expectation of a higher resistance to leakage. The determination of B* is based on analyzing the contributing factors and making an estimate of the change in leak rate that would be expected. The factors that lead to an expectation of an increase in the leak rate are as follows:

1. An increase in the primary-to-secondary differential pressure induced force on the water inside a postulated tube crack and the tube-to-tubesheet interface. For Salem Unit 1 this factor ranges from 1.70 to 1.79.

2. A decrease in the tube-to-tubesheet contact pressure above the neutral plane of the tubesheet resulting from dilation of the tubesheet holes in response to an increase in the bending deformation from the primary-to-secondary pressure difference increase. This does not apply to the periphery of the tubesheet where the opposite effect occurs.

3. A decrease in the tube-to-tubesheet contact pressure associated with the higher coefficient of thermal expansion of the tube material relative to that of the tubesheet if the temperature of the tubesheet decreases.

The factors that lead to an expectation of a decrease in the leak rate are:

1. The increase in primary pressure within the tube expands the tube into tighter contact with the tubesheet, resulting in an increase of the resistance of the material interface to flow between the tube and the tubesheet.

2. An increase in the tube-to-tubesheet contact pressure below the neutral plane of the tubesheet resulting from diminution of the tubesheet holes in response to the increase in the bending deformation from the primary-to-secondary pressure difference increase. Again, the effect is opposite for most tubes on the periphery of the tubesheet.

3. An increase in the resistance to flow associated with an increase in the viscosity of the water in the crevice if the temperature of the tubesheet decreases.

The basis for the determination of B* is the consideration of each of the above effects using results from finite element analyses of the tubesheet and results from leak rate testing of the tube-to-tubesheet interface. The analyses and testing are described Section 6.0 of this report. In summary, the leak rate is characterized by the Darcy equation for flow through a porous medium, an equation of the same form as the Hagen-Poiseuille equation for fully developed flow. The resistance to flow was developed from test data as a function of the contact pressure between the tube and the tubesheet in accord with expectations. The finite element analysis results provide calculated results for the contact pressure as a function of tube location and depth into the tubesheet based on the NOp and postulated SLB pressure and temperature conditions of the plant.

The following are discussed: background information giving a qualitative overview supporting the development of B*, flow through a crevice formulation, tube-to-tubesheet contact pressure variation, the determination of the B* distance, and conclusions regarding the B* values.

9.1 BACKGROUND INFORMATION

A natural question regarding the development and application of the B* criterion is whether or not numerical studies were performed to verify that the reduction in leak rate resistance above the neutral surface of the tubesheet associated with tubesheet bowing was adequately bounded by the increase in resistance below the neutral surface. The following discussion is intended to provide technical insight into the behavior of the leak rate from throughwall tube indications within the tubesheet by presenting:

1) the theoretical detail that is the basis for the observations from the test data and extrapolation of the test data for leak rate as function of joint length as expressed as the flow loss coefficient, and,

2) the explanation as to why the leak rate at normal operating conditions provides a bellwether for and can be used to establish a bounding value for the leak rate during steam line break conditions.

For most of the tube locations in the tubesheet the bow is convex upwards, like a dome. The tube-to-tubesheet contact pressure is an increasing linear function of the depth from the top of the tubesheet, thus, for any specified location within the tubesheet the contact pressure increases below and decreases above that location. The resistance to leakage through the tube-to-tubesheet interface is an increasing function of the contact pressure between the tube and the tubesheet. The bellwether principle is based on considering the leak rate during a postulated steam line break (SLB) event relative to that during normal operating conditions (NOp). The primary-to-secondary differential pressure during a SLB event is greater than that during NOp so that bowing of the tubesheet increases with an associated change in the slope of the contact pressure versus depth relation as a function of tube location. For all tubes, except for the small percentage of tubes located on the periphery, the slope increases. For tubes on the periphery the slope increases in an absolute sense since there is an inflection point near the periphery. Regardless, the evaluation applies because increasing the contact pressure has a greater influence on the leak rate than decreasing the contact pressure.

Numerical studies were not initially performed because the subject was considered to be adequately addressed based on a qualitative evaluation using first principles considerations as follows:

1) In the limiting case of no dependence of the leak rate loss coefficient, i.e., the resistance per unit length, on the contact pressure, the leak rate during NOp and postulated SLB would be a function of the length of the crevice and pressure difference only. Using the Darcy equation, the leak rate is a direct function of the differential pressure and the inverse of the crevice length. Since the length remains the same and the driving pressure increases by a factor of about 2, that is, 1300 psi to 2560 psi, the leak rate change is bounded by a similar factor. Any other theoretical dependence of leak rate on pressure difference, e.g., the square root of the pressure difference à la the Bernoulli equation, results in a reduction of the bounding factor relative to the result obtained using the Darcy equation.

2) The test data have demonstrated that the resistance per unit length is a monotonically increasing, non-linear function of the contact pressure with a positive second derivative. The deflection of the TS in combination with the increase in internal pressure results in the change in the contact pressure being zero between NOp and SLB at some depth below the TTS that is above the neutral surface of the tubesheet. The net contact pressure decreases above and increases below that depth, which is a function of location within the tube bundle. Using this elevation as a reference, the increase in resistance per unit length below the zero-change location must always be more than the absolute value of the decrease in resistance per unit length above the zero-change elevation. Thus, the average resistance in going from NOp to SLB must increase and the average leak rate must decrease. This is independent of the individual leak rates involved and only depends on the trend. The latter observation is apparent by inspection of the figure relating loss coefficient to contact pressure in all submittals on the subject of leak rate through tube-to-tubesheet crevices, see Figure 6-6 in this report for example.

There are alternate approaches to proving the above statements from the observations regarding the leak rate from test specimens.

9.2 FLOW THROUGH A CREVICE (DARCY'S EQUATION)

The equation that is solved for flow through a crevice is Darcy's model for flow through a porous media, that is, the volumetric flow, Q, is a function of the differential driving pressure, ΔP, and the respective inverse values of the viscosity, μ, the loss coefficient, K, and the length of the path, L, as,

$$Q = \frac{1}{\mu K}\frac{\Delta P}{L}. \qquad (9\text{-}1)$$

The driving pressure is based on the upstream minus the downstream values, else a negative sign would be needed in front of the equation. The viscosity is a function of the temperature and pressure of the fluid. The Darcy equation is also of the same form as the Hagen-Poiseuille flow equation for fully developed, laminar, axial flow in an annular gap, i.e.,

$$Q = \frac{1}{\mu \frac{6}{\pi R a^3}}\frac{\Delta P}{L}. \qquad (9\text{-}2)$$

Here, R is the average radius of the gap and a is a characteristic or effective gap dimension for the rough tube-to-tubesheet interface, expected to be very small, on the order of $4 \cdot 10^{-5}$ inch. Thus, the loss coefficient would be expected to be proportional to the inverse of the cube of the effective gap. The Hagen-Poiseuille form of the leak rate equation gives insight into the relationship between the average resistance, characterized by the loss coefficient, K, and the contact pressure, i.e.,

$$K = \frac{6}{\pi R a^3}. \qquad (9\text{-}3)$$

If the characteristic gap were proportional to the contact pressure between the tube and the tubesheet, doubling the pressure would increase the leak resistance by a factor of 8, although this is not necessarily

expected to be the case because of the complex nature of the interface. In addition, it would not be unexpected that a plot of the ln(K) versus the contact pressure would approximate a straight line. For the rough tube-to-tubesheet interface, the length of the tortuous path can also be considered to be characterized as being effective because the flow does not necessarily have a straight path to follow to the TTS (top of the tubesheet). Approximation of the path as the legs of an equilateral triangle would essentially double the distance traveled from the throughwall location to the TTS. Hence, the use of the loss coefficient integrates the accounting of the effective gap and effective length.

The electrical analogy for the flow considers Q as the current flow and ΔP as the potential, hence the quantity μKL is the resistance to flow, R. Since K is a function of the contact pressure, P_c, the resistance is a function of the location within the tubesheet. The total resistance can be found as the average value of the quantity μK, the resistance per unit length, multiplied by L, or by integrating the incremental resistance, $dR = \mu K\, dL$ over the length L, i.e.,

$$R = \mu \bar{K}\left(L_2 - L_1\right) = \int_{L_1}^{L_2} \mu K\, dL, \qquad (9\text{-}4)$$

where both μ and K could be functions of location L. The viscosity is a very weak function of the pressure of the water in the crevice and can be considered to be constant for a given plant condition with negligible error, Figure 9-13, References 37 and 38. However, the viscosity is a strong function of the temperature of the water in the crevice, Figure 9-14, and the tubesheet temperature for the condition being analyzed must be considered. A decrease in the temperature can lead to a significant increase in flow resistance.

9.3 TUBE-TO-TUBESHEET CONTACT PRESSURE VARIATION

Six tubesheet radial locations for which the contact pressure as a function of depth was determined were used in calculating the length of sound tubing below the TTS required to resist the NOp and SLB axial loads, i.e., the H* depth. See Figure 9-1. The intercept, b_0, and slope, b_1, parameters for the calculation of the contact pressure as a function of length, L, into the tubesheet for the six radial locations are listed in Table 9-1. The relationships are always in linear first order form,

$$P_c = b_0 + b_1 L \qquad (9\text{-}5)$$

where the coefficients b_0 and b_1 vary as a function of the radial location of the tube in the tubesheet. This is simply a consequence of the fact that in the linear elastic stress analysis, no yielding occurs. A comparison of the FEA results with first order, linear representations is provided on Figure 9-5 for NOp and SLB conditions at a radius of 34 inches from the center of the tubesheet.

Further calculations examined the relationship between the intercept and slope of the prediction equations as a function of tube location radius. It was found that second order polynomial expressions can be used to describe the parameters almost exactly, i.e., with negligible error. A plot of the operating contact pressures, which do not include the residual contact pressure from the hydraulic expansion process, is provided on Figure 9-6 for each location during NOp and Figure 9-7 provides similar information during the postulated SLB event. The polynomial coefficients that were used to determine the values of the intercept and slope, i.e., b_0 and b_1, for any given radius, R, from the center of the tubesheet for NOp conditions are illustrated on Figure 9-8. Here, the following relationships are depicted where the g and h values were determined from the regression analyses (recall that R in the following two equations

represents radius),

Normal Operation

$$\begin{aligned} b_o &= g_0 + g_1 R + g_2 R^2 \quad \text{(Intercept)} \\ b_1 &= h_0 + h_1 R + h_2 R^2 \quad \text{(Slope)} \end{aligned} \quad . \qquad (9\text{-}6)$$

The coefficients, u and v, of a similar set of expressions were calculated for determining the contact pressure at all locations within the tubesheet during a SLB event, i.e.,

SLB Conditions

$$\begin{aligned} b_o &= u_0 + u_1 R + u_2 R^2 \quad \text{(Intercept)} \\ b_1 &= v_0 + v_1 R + v_2 R^2 \quad \text{(Slope)} \end{aligned} \quad . \qquad (9\text{-}7)$$

The polynomial coefficients that were used to determine the values of the intercept and slope for use in calculating the contact pressure during a SLB are illustrated on Figure 9-9. A comparison of the coefficients for the two conditions is provided on Figure 9-10.

9.4 DETERMINATION OF THE B* DISTANCE

The results from multiple leak rate testing programs indicate that the logarithm of the loss coefficient is a linear function of the contact pressure, i.e.,

$$\ln K = a_0 + a_1 P_c , \qquad (9\text{-}8)$$

where the coefficients, a_0 and a_1 of the linear relation are found from a regression analysis of the test data; both coefficients are greater than zero. Simply put, the loss coefficient is greater than zero at the point where the contact pressure is zero and the loss coefficient increases with increasing contact pressure. Thus,

$$K = e^{a_0 + a_1 P_c} , \qquad (9\text{-}9)$$

and the loss coefficient is an exponential function of the contact pressure. Combining Equation 9-9 for the loss coefficient as a function of the contact pressure with Equation 9-5 for the contact pressure as a function of length yields,

$$K = e^{a_0 + a_1 (b_0 + b_1 L)} = e^{c_0 + c_1 L} \qquad (9\text{-}10)$$

where L is reckoned downward from the lower of the top of the tubesheet or the bottom of the expansion transition and the joined coefficients are given by $c_0 = a_0 + a_1 b_0$ and $c_1 = a_1 b_1$. Away from the periphery of the tubesheet, b_1 is greater than zero, hence c_1 is also greater than zero and the loss coefficient increases with depth into the tubesheet. Alternatively, the relation also means that near the periphery of the tubesheet the resistance to flow increases above any depth when the tubesheet bows upward. Since the B* distance into the tubesheet is based on finding the depth for which the resistance to leak during SLB is the same as that during NOp, the meaningful radial region of the tubesheet is away from the periphery, that is, where the resistance to leakage decreases near the top of the tubesheet. Another point to note from the above expression is that in the region of interest the second derivative of the loss coefficient with respect to depth is positive. This means that the resistance per unit length is always increasing with depth into the

tubesheet. One consequence of the relation is that the decrease in resistance for a specified distance above any reference point is balanced by the increase in resistance over a shorter distance below that reference point. The coefficients for the contact pressure as a function of location are given by Equations 9-6 and 9-7 for NOp and SLB respectively.

The B* distance is designated by L_B in the following equations and is the depth at which the resistance to leak during SLB is the same as that during NOp. Note that the product of the viscosity and the loss coefficient is the resistance per unit length for any location in the tubesheet. The resistance to leak, R, as a function of the viscosity, μ, average loss coefficient, $\overline{K}$, and length of the leak path from some uppermost location, L_0, to L_B for any condition is given by,

$$R = \mu\overline{K}\left(L_B - L_0\right) = \mu\int_{L_0}^{L_B} e^{c_0 + c_1 L}\,dL. \qquad (9\text{-}11)$$

The limits of the integration define the range over which there is a contact pressure between the tube and the tubesheet that is greater than zero, i.e., ignoring any resistance to flow above that elevation. The lower limit is the lower of the TTS, the BET (bottom of the expansion transition), or the point where the contact pressure is zero. Carrying out the integration,

$$R = \mu\frac{e^{c_0}}{c_1}\left[e^{c_1 L_B} - e^{c_1 L_0}\right]. \qquad (9\text{-}12)$$

The equation can be used directly when the point of zero contact pressure between the tube and the tubesheet is at or below the TTS or BET, whichever is lower.

In order to account for the condition wherein L_0 is < 0 (See Figure 9-3 and 9-4), i.e., at or above the TTS or BET, whichever is lower, the equation is written as,

$$R = \mu\frac{e^{c_0}}{c_1}\left[e^{c_1 L_B} - \mathrm{if}\left(L_0 > 0, e^{c_1 L_0}, 1\right)\right]. \qquad (9\text{-}13)$$

Here, the first argument of the "if" statement is the condition to be tested, the second argument is the value used if the condition is true, and the third argument is used if the condition is false, that is, when zero contact pressure is predicted above the TTS or BET (See Figure 9-4). For normal operation the resistance to leakage is given by,

$$R_N = \mu_N\frac{e^{c_{0N}}}{c_{1N}}\left[e^{c_{1N} L_B} - \mathrm{if}\left(L_{0N} > 0, e^{c_{1N} L_{0N}}, 1\right)\right], \qquad (9\text{-}14)$$

and for SLB by,

$$R_S = \mu_S\frac{e^{c_{0S}}}{c_{1S}}\left[e^{c_{1S} L_B} - \mathrm{if}\left(L_{0S} > 0, e^{c_{1S} L_{0S}}, 1\right)\right]. \qquad (9\text{-}15)$$

The B* distance is such that the resistance during SLB is the same as that during NOp, limiting the leak rate to be no more than a factor of two times that during normal operation, thus the solution is obtained for the value of L_B that makes $R_S = R_N$.

For the Salem Unit 1 SGs, the value of B* varies from a maximum of 6.83 inches at a radius of 2 inches, about row 1 at the center of the bundle, to less than 6 inches at a radius of 15 inches. Above a radius of 32 inches from the center of the tubesheet, the integrated leak resistance is always greater during a SLB event than during NOp. A plot of the calculated B* values is provided on Figure 9-15. Here, any values less than 1 inch were truncated to 1 inch. For example, the top of the tubes at the extreme periphery of the tubesheet are in compression during NOp because of contraction of the tube holes due to convex downward bending. The level of compression increases during a SLB event because the magnitude of the convex bending increases. Thus, any leak rate during NOp would bound the leak rate during SLB. While the driving pressure would increase by a factor of up to 2, the contact pressure between the tube and the tubesheet would increase toward the top of the tubesheet. For any radial location in the Salem Unit 1 SGs, for indications below 6.83 inches from the top of the tubesheet, any leak rate during SLB conditions would be bounded by 2 times the leakage during normal operating conditions. A summary of the B* values for Salem Unit 1 is provided in Table 9-2. A plot of nominal B* values as a function of tubesheet radius where there would be no change in leak rate during a postulated SLB is provided in Figure 9-16. Figure 9-17 represents a graphical determination of B* depth based on comparing integrated resistance to depth from the top of the tubesheet at a radius of 8 inches from the center of the tube bundle.

9.5 SENSITIVITY OF THE B* CALCULATION

Additional insight into the effect of cold leg indications on the relative leak rate is illustrated by examining the plots of TTS contact pressure on Figure 9-12 for the hot leg and Figure 9-18 for the cold leg. The change in differential pressure and the change in temperature result in the contact pressure at the TTS decreasing relative to the hot leg. This means that leakage during NOp operation on the cold leg will be closer to that during a postulated SLB event and the relative increase smaller. The conclusion regarding the cold leg indications is that the application of a hot leg derived B* is conservative.

9.6 ADDITIONAL LEAKAGE SENSITIVITY STUDY FOR 8 INCH INSPECTION LENGTH CRITERIA COMPLETED FOR 11/16 OD TUBING

The basis for the development of the 8-inch tubesheet inspection zone with regard to leak rate is the ratio of the potential leak rate during a SLB event to that during NOp using the results of data from leak rate tests of the tube-to-tubesheet interface, a.k.a. crevice. Westinghouse had historically developed a computer model for a crevice in series with a crack using the crevice data and independent data for free-span cracks. The NRC staff has expressed concerns regarding the model because of a lack of test data from physical specimens which contained a crack in series with a crevice. Westinghouse data obtained from separate testing of the tube-to-tubesheet crevice and axial cracks within a tubesheet with a zero length crevice above the crack demonstrated the resistance of the crack to be comparable to the resistance of the crevice for a larger tube size. The implication from the latter being that an analysis that neglected the effect of the crack would be valid because the effect on the numerator and denominator of the SLB:NOp leak rates ratio would be the same. Other considerations were also made, e.g., for indications within about 56 inches from the center of the tubesheet, the effect of tubesheet bow induced crack closure would be to increase the resistance of the crack. If the geometry resistance to flow is about constant between the two conditions (at a depth below 8 inches it would be expected to increase) and if the fluid resistance to flow is about constant (if the temperature decreases it would be expected to increase), the leak rate would behave as the SLB:NOp differential pressure ratio, or the square root of the ΔP ratio, hence the factor of 2.

It is also worth noting the expectations from the NRC requested analyses based on the crack opening area formulations and the geometries inherent in the model analysis. The opening of circumferential cracks is resisted by the stiffness of the material above and below the crack flanks and by friction on the OD of the tube. For all practical purposes the tube is infinitely long in the axial direction, although, the resistance to opening due to the shear interface increases rapidly. The geometry of the tubesheet does not restrict crack opening in the axial direction. The opening of axial cracks is more restricted owing to the geometry of the problem. For example, there is a line of symmetry 180° from the crack flanks, hence the tube is built-in at that location. There is also friction associated with the interface of the tube with the tubesheet. Finally, the confinement provided by the tubesheet means that an axial crack cannot open more than the dilation of the tubesheet hole, which ranges from compression near the center to being very small on the periphery. Thus, the effects reported from the DENTFLO analyses would be expected to indicate more effect for circumferential cracks than for axial cracks.

In trying to use the Westinghouse computer model to provide a qualitative demonstration of the veracity of the B* analysis, a significant potential shortcoming associated with the approach was recognized, although it would be conservative relative to predicting actual leak rates. The crack flow leak rate model

portion of the code was based on a freespan axial crack, not a crack with flanks constrained by the tubesheet hole. Thus, the crack opening area computation could be significantly biased. In order to perform the requested studies the code was modified as follows:

1) The crack opening area model for circumferential cracks of Appendix C of WCAP-15932 was included in the DENTFLO code.

2) A crack opening area model for axial cracks constrained in the tubesheet was derived accounting for the constraints added to the problem by the presence of the tubesheet and included in the DENTFLO code.

The new model for the axial crack opening that takes into account the guidance and constraint provided by the surrounding tubesheet is described in what follows (See Figure 9-19). [

]a,c,e

Using these models, a sensitivity study was performed which consisted of a series of analyses to demonstrate the conservatisms of the bellwether approach. These analyses consider the locations specified in the NRC RAI, and the conditions specified in the NRC RAI, i.e., crack only, crevice only and combined crack and crevice.

The models developed for these analyses are for qualitative comparisons only (i.e., not for absolute prediction of leak rates) and were not verified and/or validated beyond their current "information" status since they are for comparative purposes only.

Constrained Axial Crack Opening Area Calculations

A literature search of the significant fracture mechanics texts and journals (e.g. Journal of Engineering Fracture Mechanics, International Journal of Fracture Mechanics, Reference 50, etc.) did not yield any previously published models for the crack opening area for a central axial crack that is circumferentially constrained in an internally pressurized tube. A new model was developed to calculate the crack opening area of a constrained SG tube under these conditions. The general form of the equation for the crack opening area in an infinite plate is (Reference 44):

$$\mathrm{COA} = 2\pi a^2 \frac{\sigma}{E}$$

where σ is the far-field stress resulting in the crack opening, E is the Young's Modulus of the tube and a is the half-length of the crack. In the absence of empirical evidence, the general form for the crack opening area can be modified by a functional. The functional can include the important details regarding the boundary conditions and other effects relative to the constrained and cracked tube. Let $H(\,F(n)\,)$ be the modifying functional for the crack opening area, where n represents the influential parameters of the geometry and loading. Then the model for the crack opening area becomes,

$$\mathrm{COA} = H\big(\,F(n)\,\big)2\pi a^2 \frac{\sigma}{E}$$

Given the general constitutive form for the crack opening area it remains to define the modifying functional. Based on previous work (Reference 41) it is reasonable to assume that the crack opening area for the axial crack would be affected by the interactions of the internal pressure on the crack flanks and the contact pressure between the tube and the tube sheet. The largest effect on the crack flanks that can affect the flow rate through the crack will be the bending of the flanks due to the internal pressure in the event that the contact pressure between the tube and the tube sheet decreases, although, as previously noted, the amount of opening can never be greater than the change in the circumference of the hole in the tubesheet.

Comparison Against Established Methods

The resulting model for the crack opening area in an internally pressurized and constrained tube with an axial crack [

]a,c,e

where ζ is a scalar coefficient that describes the local effect of the crack in a tube on an equivalent finite flat plate area and ϕ is a parameter that accounts for the change in resistance to bending of the crack flanks due to the change in tube to tubesheet contact pressure and crack length. This model was compared against several established models for unconstrained internally pressurized tubes with axial cracks. These comparison models include published work by Zahoor (Reference 46) and empirical models employed by the American Petroleum Institute (API) and developed by Anderson (Reference 48). Figure 9-20 shows the results that each model predicts for the crack opening area for cracks ranging from 0.02 to 2.00 inch.

The comparison shows that the results for the new model, for a constrained internally pressurized tube, are reasonable in comparison to the other established models for unconstrained tubes. Specifically, the new model predicts a smaller crack opening area as a function of crack length than the unconstrained axial cracks with a smaller rate of increase in the crack opening area. This is an expected result because the other models assume a free span for the tube with no constraining effects. Therefore it is reasonable to use the new model for further calculations to estimate the crack opening area of a constrained and internally pressurized tube with a central axial crack.

Analysis of Circumferential Cracking

A model for a constrained circumferential crack in an internally pressurized tube was developed (Reference 45). This model is appropriate to use for a circumferential crack that occurs in a tube within the tubesheet. A maximum crack half angle of 90°, or a maximum circumferential crack equal to half of the circumference of the tube, was used in this analysis as a simplifying assumption. The model for constrained circumferential cracks was implemented in the code DENTFLO to determine the trend of the leakage rate ratios for normal operating and steam line break conditions. See References 45 and 48 for more details on the model and its application.

DENTFLO Analysis Methodology

The program DENTFLO was run to determine the trend of the leak rate ratios of a damaged tube at the bottom of the tubesheet at different radii. There are 36 different cases of interest with respect to the leak rate ratio analysis. The 36 cases are comprised of: 2 thermal-hydraulic conditions (NOp and SLB), 2 crack orientations (axial and circumferential), 3 radial locations (near, mid, and peripheral) and 3 conditions of interest (crack, crevice, and combined crack and crevice). The Normal Operation – Maximum Temperature (NOp-MAX) and the Steam Line Break (SLB) were chosen for the analysis because of the largest change in temperature and pressure between the two cases. The tubesheet radii for each range are: Near (2.0774 In), Mid (33.101 In), Peripheral (60.2475 In).

The contact pressures used in the DENTFLO analysis were taken at each tubesheet radius at several elevations in the tubesheet (Reference 51). The tubesheet material below 4.00 from the bottom of the tubesheet," and any contact pressure generated by that material, was conservatively neglected. The loss coefficients for the analysis were taken as a numerically integrated average over the length of the crevice (i.e. from 4" above the bottom of the tubesheet to the top of the tubesheet) based on the contact pressure distribution in the tubesheet (Reference 49). The axial and circumferential crack orientation cases used the models discussed above. The crevice only cases use a model for an unconstrained axial crack based on the work of Paris and Tada [44] because it gives less resistance to flow through a crack than the other models available in DENTFLO. The crack length is also large in the crevice only case in order to best represent a situation where the crack cannot contribute significantly to the flow resistance.

DENTFLO Analysis Results

The cases described above were analyzed in DENTFLO. See Figure 9-21 and Figure 9-22 for the typical results of the leak rate ratio as a function of the crack length for the axial and circumferential cracks at the peripheral tubesheet radius. In each of the figures below, the crack length is in terms of $2a$, or twice the half-length of the crack. The leak rate ratio is in terms of the SLB condition leak rate in GPM divided by the NOp condition leak rate in GPM.

Each figure includes the results for the leak rate ratio of a crack and crevice in series and crack only cases. The crevice only cases were also run using DENTFLO, but as they use a single value for the crack length, the results are not shown in the figures above. The analysis results show that for a crack and a crevice in series, with a crack larger than 0.50" in the near range, the leak rate ratio plateaus at a value less than 2.00 for both circumferential and axial cracks. The leak rate calculations of the values for cracks 0.50" and smaller should be discounted given the asymtotic nature of the equations. This is because such a small

crack is equivalent to a point source or a singularity in the fluid flow equations and in reality is not likely to cause any significant leakage. The crevice only near radius leakage rate ratio results are also less than 2.00. The results from the crack only analyses show that in the absence of the dent the resistance to flow is increased and each crack type produces a lower leak rate ratio. The average and maximum values of the leak rate ratios for the near radius results are summarized in Tables 9-3, 9-4 and 9-5.

The mid range radius results also indicate that for a crack and a crevice in series, for cracks 0.50" and larger, the leak rate ratio plateaus at a value less than 2.00 for the different crack orientations. The crevice only leak rate ratio results are much smaller than 2.00. The crack only ratio results are smaller than the crack and crevice in series. The average and maximum values of the leak rate ratios for the mid radius results are summarized in Table 9-4.

Figure 9-21 and Figure 9-22 show the results for the peripheral cases. [

]a,c,e

9.7 CONCLUSIONS RELATIVE TO B*

The resistance equations above can be used to show that the resistance is always bounded by the region through the thickness of the tubesheet where the contact pressure increases relative to the region where the contact pressure decreases. This simply means that the leak rate resistance increases during a SLB event relative to that during normal operation for similar lengths about a reference depth from the TTS, for example, B*. The resistance equations were used to calculate B* as the distance from the TTS for which the resistance during a postulated SLB event is the same as that during NOp. This means that the leak rate during SLB from any and all indications below B* will be bounded by a multiple of the leak rate during NOp based on the relative driving pressure for the two conditions. The differential pressure ratio during a SLB at Salem Unit 1 ranges from 1.70 to 1.79, for the cases described in Section 8.0, times that during NOp depending on the operating conditions considered, e.g., differences in plugging level. Hence, the leak rate during a postulated SLB event would be expected to be no more than 2 times the leak rate being experienced during normal operation. Moreover, the B* analysis did not take into consideration the effect of the increase in the contact pressure below the B* elevation on the leak rate through postulated tube cracks within the tubesheet. For axial cracks the flanks would be compressed and the leak rate through the cracks themselves would be expected to decrease. For circumferential cracks the resistance to flank displacement in the axial direction would be expected to negate the effect of the slight increase in pressure on the crack flanks. In conclusion, the use of a factor of 2 would be expected to be conservative.

Table 9-1. First Order Equation Coefficients for the Variation of Contact Pressures Through Tubesheet

a,c,e

Table 9-2. B* Summary Table Leak Rate Required Engagement Lengths			
Zone	Leak Resistance Ratio R_{SLB} / R_{NOp} (1, 2)	Engagement from TTS (inches)	
		Hot Leg	Cold Leg
A	1.0	1.00	1.00
B	1.0	1.00	1.00
C	1.0	5.78	3.60
D	1.0	6.83	5.05

Notes:
1. Conditions for the analyses are specified in Sections 7.1.4.1 for NOp, and 7.1.4.3 for SLB.
2. Equal resistance assures leak rate during accident conditions is not more than twice that during normal operation.
3. H* structural summary is provided in Section 7.2.

Table 9-3: Summary of Near Radius Leak Rate Ratio Results

a,c,e

[ ]

Table 9-4: Summary of Mid Radius Leak Rate Ratio Results

a,c,e

[ ]

Table 9-5: Summary of Peripheral Radius Leak Rate Ratio Results

a,c,e

[ ]

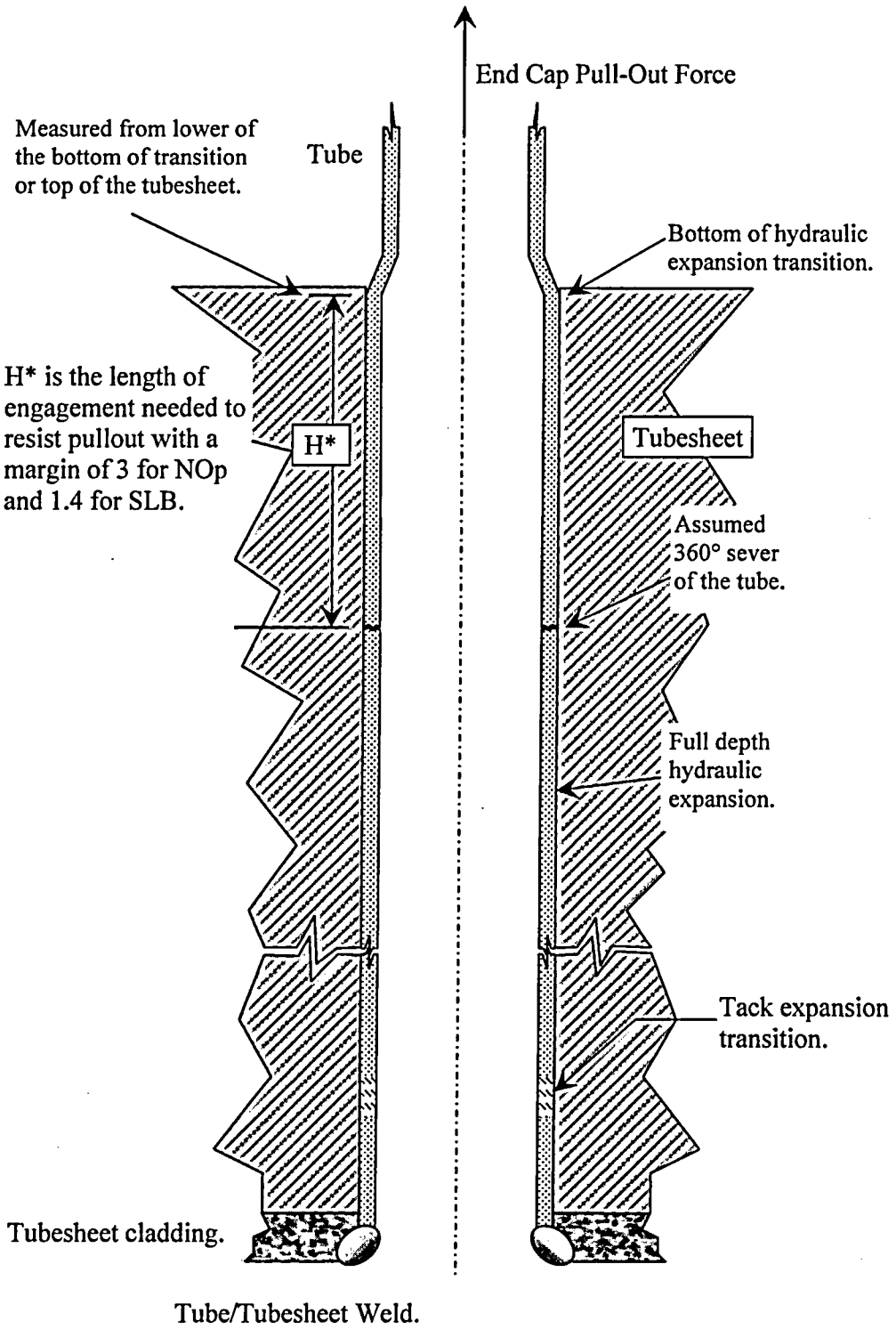

Figure 9-1. Determination of H*

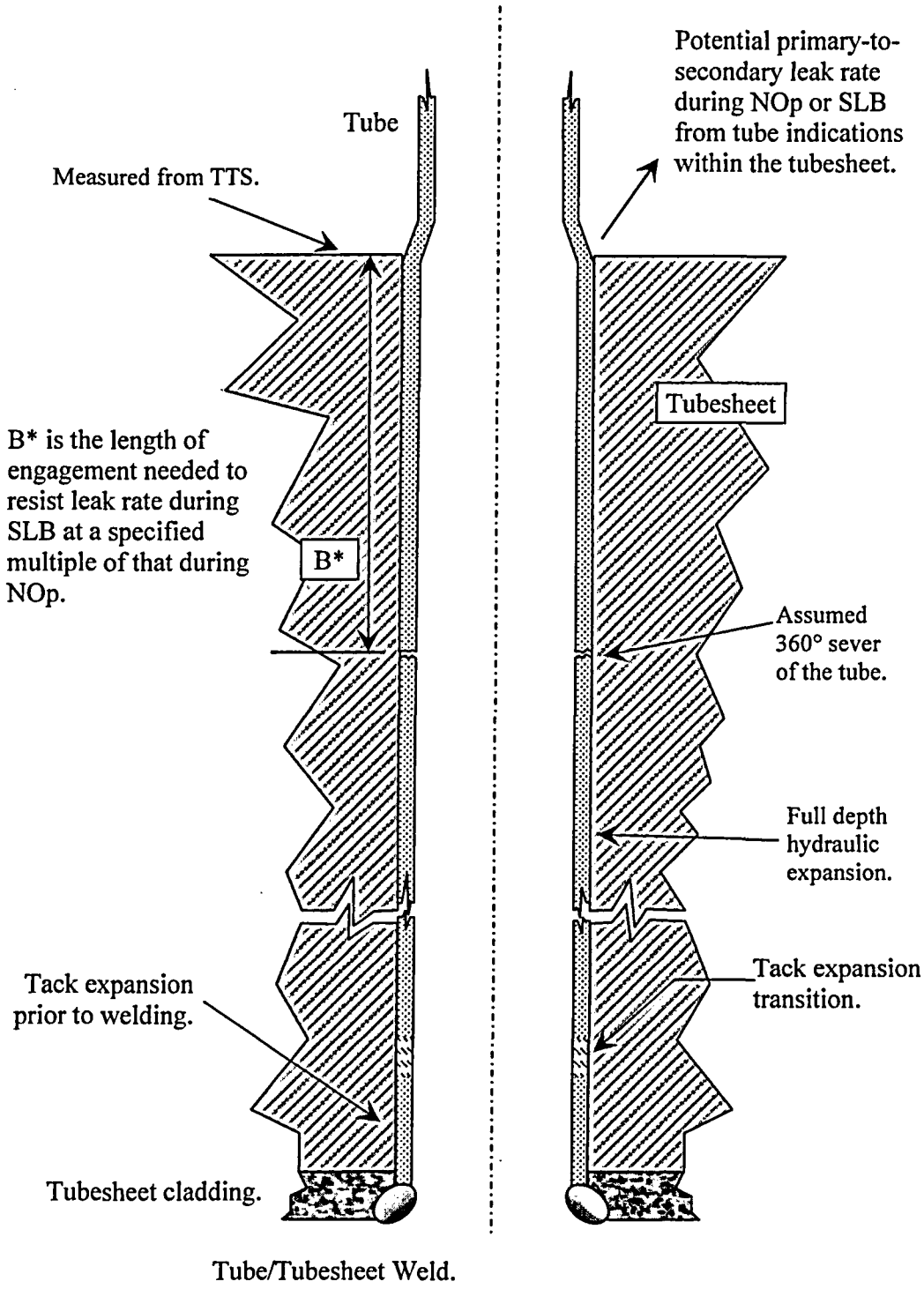

Figure 9-2. Determination of B*

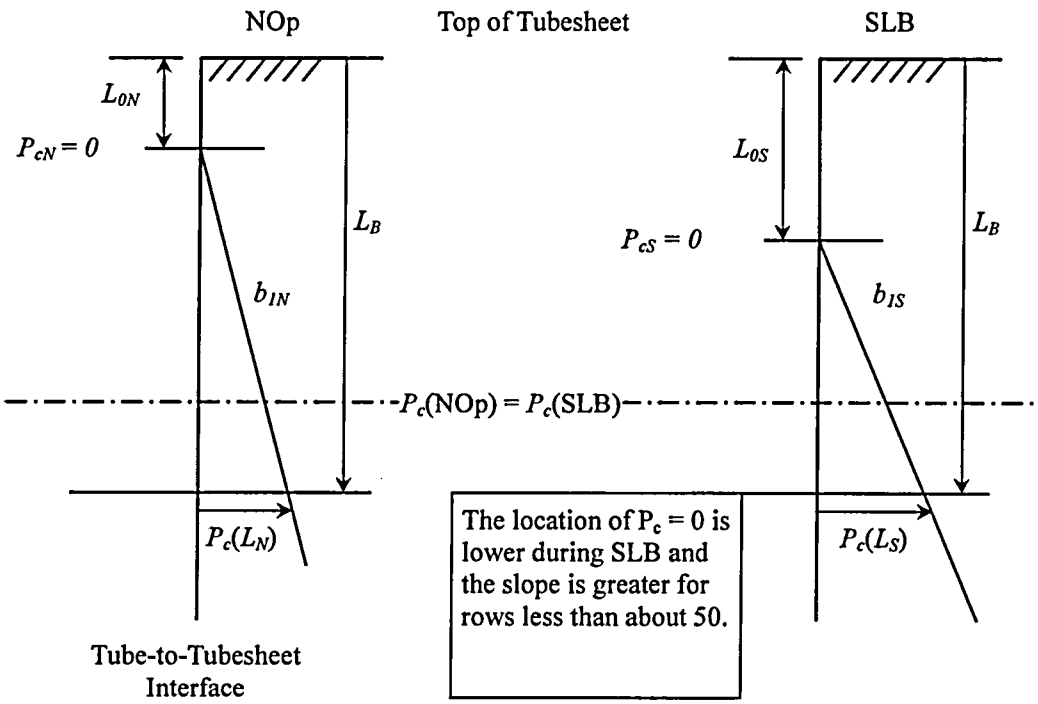

Figure 9-3. Concepts for the Determination of B*

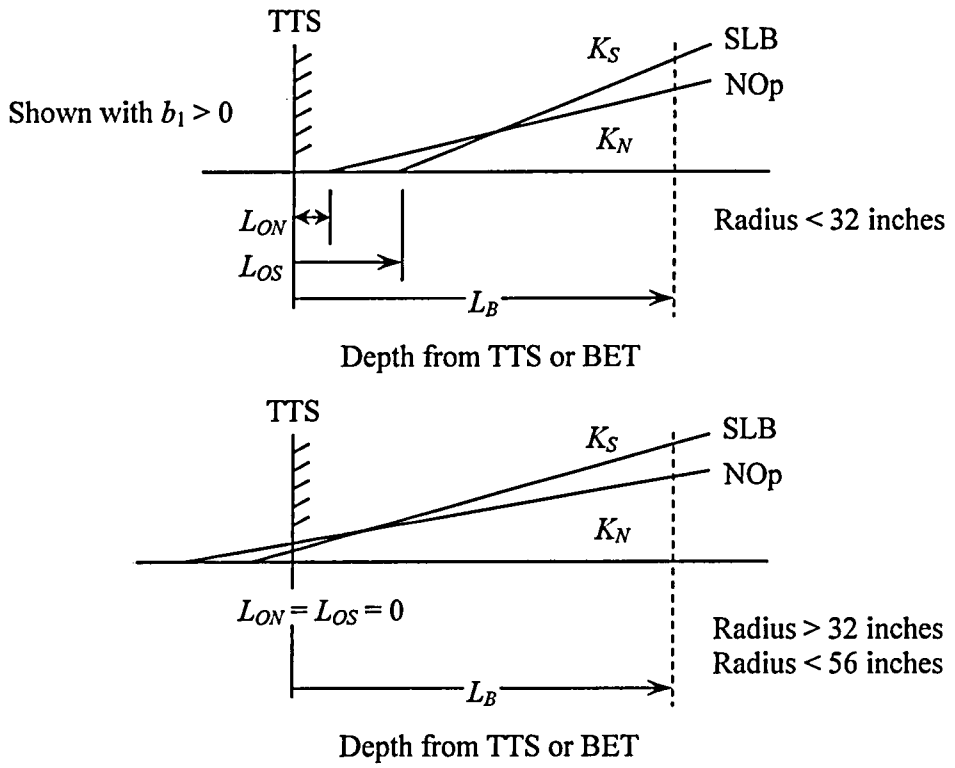

Figure 9-4. Schematic for the Determination of B* Parameters

a,c,e

Figure 9-5. First Order Linear Representation of Contact Pressure

a,c,e

Figure 9-6. Contact Pressure During Normal Operation (Model F)

a,c,e

Figure 9-7. Contact Pressure During SLB (2560 psi at 420°F)

AFFIDAVIT FOR WITHHOLDING PROPRIETARY INFORMATION FROM PUBLIC DISCLOSURE IN ACCORDANCE WITH 10CFR2.390

(Westinghouse Letter CAW-06-21 96, August 31,2006)

Westinghouse

Westinghouse Electric Company
Nuclear Services
P.O. Box 355
Pittsburgh, Pennsylvania 15230-0355
USA

U.S. Nuclear Regulatory Commission
Document Control Desk
Washington, DC 20555-0001

Direct tel: (412) 374-4643
Direct fax: (412) 374-4011
e-mail: greshaja@westinghouse.com

Our ref: CAW-06-2196

August 31, 2006

APPLICATION FOR WITHHOLDING PROPRIETARY
INFORMATION FROM PUBLIC DISCLOSURE

Subject: WCAP-16640-P, "Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet at Salem Unit 1," August 2006 (Proprietary)

The proprietary information for which withholding is being requested in the above-referenced report is further identified in Affidavit CAW-06-2196 signed by the owner of the proprietary information, Westinghouse Electric Company LLC. The affidavit, which accompanies this letter, sets forth the basis on which the information may be withheld from public disclosure by the Commission and addresses with specificity the considerations listed in paragraph (b)(4) of 10 CFR Section 2.390 of the Commission's regulations.

Accordingly, this letter authorizes the utilization of the accompanying affidavit by PSEG Nuclear LLC.

Correspondence with respect to the proprietary aspects of the application for withholding or the Westinghouse affidavit should reference this letter, CAW-06-2196, and should be addressed to J. A. Gresham, Manager, Regulatory Compliance and Plant Licensing, Westinghouse Electric Company LLC, P.O. Box 355, Pittsburgh, Pennsylvania 15230-0355.

Very truly yours,

J. A. Gresham, Manager
Regulatory Compliance and Plant Licensing

Enclosures

cc: G. Shukla/NRC

bcc: J. A. Gresham (ECE 4-7) 1L
R. Bastien, 1L (Nivelles, Belgium)
C. Brinkman, 1L (Westinghouse Electric Co., 12300 Twinbrook Parkway, Suite 330, Rockville, MD 20852)
RCPL Administrative Aide (ECE 4-7A) 1L, 1A (letter and affidavit only)
G. W. Whiteman, Waltz Mill
H. O. Lagally Waltz Mill
N. R. Brown, Waltz Mill
K. Harsche, ECE 558D
D. Lockridge, ECE 557F
J. P. Molkenthin, Windsor
C. D. Cassino, Waltz Mill

CAW-06-2196

AFFIDAVIT

COMMONWEALTH OF PENNSYLVANIA:

ss

COUNTY OF ALLEGHENY:

Before me, the undersigned authority, personally appeared B. F. Maurer, who, being by me duly sworn according to law, deposes and says that he is authorized to execute this Affidavit on behalf of Westinghouse Electric Company LLC (Westinghouse), and that the averments of fact set forth in this Affidavit are true and correct to the best of his knowledge, information, and belief:

B. F. Maurer, Principal Engineer
Regulatory Compliance and Plant Licensing

Sworn to and subscribed before
me this 31st day of August, 2006

Sharon L. Fiori
Notary Public

Notarial Seal
Sharon L. Fiori, Notary Public
Monroeville Boro, Allegheny County
My Commission Expires January 29, 2007
Member, Pennsylvania Association Of Notaries

(1) I am Principal Engineer in Regulatory Compliance and Plant Licensing, Nuclear Services, Westinghouse Electric Company LLC (Westinghouse), and as such, I have been specifically delegated the function of reviewing the proprietary information sought to be withheld from public disclosure in connection with nuclear power plant licensing and rule making proceedings, and am authorized to apply for its withholding on behalf of Westinghouse.

(2) I am making this Affidavit in conformance with the provisions of 10 CFR Section 2.390 of the Commission's regulations and in conjunction with the Westinghouse "Application for Withholding" accompanying this Affidavit.

(3) I have personal knowledge of the criteria and procedures utilized by Westinghouse in designating information as a trade secret, privileged or as confidential commercial or financial information.

(4) Pursuant to the provisions of paragraph (b)(4) of Section 2.390 of the Commission's regulations, the following is furnished for consideration by the Commission in determining whether the information sought to be withheld from public disclosure should be withheld.

(i) The information sought to be withheld from public disclosure is owned and has been held in confidence by Westinghouse.

(ii) The information is of a type customarily held in confidence by Westinghouse and not customarily disclosed to the public. Westinghouse has a rational basis for determining the types of information customarily held in confidence by it and, in that connection, utilizes a system to determine when and whether to hold certain types of information in confidence. The application of that system and the substance of that system constitutes Westinghouse policy and provides the rational basis required.

Under that system, information is held in confidence if it falls in one or more of several types, the release of which might result in the loss of an existing or potential competitive advantage, as follows:

(a) The information reveals the distinguishing aspects of a process (or component, structure, tool, method, etc.) where prevention of its use by any of Westinghouse's competitors without license from Westinghouse constitutes a competitive economic advantage over other companies.

(b) It consists of supporting data, including test data, relative to a process (or component, structure, tool, method, etc.), the application of which data secures a competitive economic advantage, e.g., by optimization or improved marketability.

(c) Its use by a competitor would reduce his expenditure of resources or improve his competitive position in the design, manufacture, shipment, installation, assurance of quality, or licensing a similar product.

(d) It reveals cost or price information, production capacities, budget levels, or commercial strategies of Westinghouse, its customers or suppliers.

(e) It reveals aspects of past, present, or future Westinghouse or customer funded development plans and programs of potential commercial value to Westinghouse.

(f) It contains patentable ideas, for which patent protection may be desirable.

There are sound policy reasons behind the Westinghouse system which include the following:

(a) The use of such information by Westinghouse gives Westinghouse a competitive advantage over its competitors. It is, therefore, withheld from disclosure to protect the Westinghouse competitive position.

(b) It is information that is marketable in many ways. The extent to which such information is available to competitors diminishes the Westinghouse ability to sell products and services involving the use of the information.

(c) Use by our competitor would put Westinghouse at a competitive disadvantage by reducing his expenditure of resources at our expense.

(d) Each component of proprietary information pertinent to a particular competitive advantage is potentially as valuable as the total competitive advantage. If competitors acquire components of proprietary information, any one component may be the key to the entire puzzle, thereby depriving Westinghouse of a competitive advantage.

(e) Unrestricted disclosure would jeopardize the position of prominence of Westinghouse in the world market, and thereby give a market advantage to the competition of those countries.

(f) The Westinghouse capacity to invest corporate assets in research and development depends upon the success in obtaining and maintaining a competitive advantage.

(iii) The information is being transmitted to the Commission in confidence and, under the provisions of 10 CFR Section 2.390; it is to be received in confidence by the Commission.

(iv) The information sought to be protected is not available in public sources or available information has not been previously employed in the same original manner or method to the best of our knowledge and belief.

(v) The proprietary information sought to be withheld in this submittal is that which is appropriately marked in WCAP-16640-P, "Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet at Salem Unit 1," dated August 2006 (Proprietary). The information is provided in support of a submittal to the Commission, being transmitted by PSEG Nuclear LLC for Withholding Proprietary Information from Public Disclosure, to the Document Control Desk. The proprietary information as submitted for use by Westinghouse for Salem Unit 1 is expected to be applicable to other licensee submittals in support of implementing a limited inspection of the tube joint with a rotating probe within the tubesheet region of the steam generators.

This information is part of that which will enable Westinghouse to:

(a) Provide documentation of the analyses, methods, and testing for the implementation of an alternate repair criteria for the portion of the tubes within the tubesheet of the Salem Unit 1 steam generators.

(b) Provide a primary-to-secondary side leakage evaluation for the Salem Unit 1 steam generators during all plant conditions.

(c) Assist the customer to respond to NRC requests for information.

Further this information has substantial commercial value as follows:

(a) Westinghouse plans to sell the use of similar information to its customers for purposes of meeting NRC requirements for licensing documentation.

(b) Westinghouse can sell support and defense of this information to its customers in the licensing process.

(c) The information requested to be withheld reveals the distinguishing aspects of a methodology which was developed by Westinghouse.

Public disclosure of this proprietary information is likely to cause substantial harm to the competitive position of Westinghouse because it would enhance the ability of competitors to provide similar licensing support documentation and licensing defense services for commercial power reactors without commensurate expenses. Also, public disclosure of the information would enable others to use the information to meet NRC requirements for licensing documentation without purchasing the right to use the information.

The development of the technology described in part by the information is the result of applying the results of many years of experience in an intensive Westinghouse effort and the expenditure of a considerable sum of money.

In order for competitors of Westinghouse to duplicate this information, similar technical programs would have to be performed and a significant manpower effort, having the requisite talent and experience, would have to be expended.

Further the deponent sayeth not.

PROPRIETARY INFORMATION NOTICE

Transmitted herewith are proprietary and/or non-proprietary versions of documents furnished to the NRC in connection with requests for generic and/or plant-specific review and approval.

In order to conform to the requirements of 10 CFR 2.390 of the Commission's regulations concerning the protection of proprietary information so submitted to the NRC, the information which is proprietary in the proprietary versions is contained within brackets, and where the proprietary information has been deleted in the non-proprietary versions, only the brackets remain (the information that was contained within the brackets in the proprietary versions having been deleted). The justification for claiming the information so designated as proprietary is indicated in both versions by means of lower case letters (a) through (f) located as a superscript immediately following the brackets enclosing each item of information being identified as proprietary or in the margin opposite such information. These lower case letters refer to the types of information Westinghouse customarily holds in confidence identified in Sections (4)(ii)(a) through (4)(ii)(f) of the affidavit accompanying this transmittal pursuant to 10 CFR 2.390(b)(1).

COPYRIGHT NOTICE

The reports transmitted herewith each bear a Westinghouse copyright notice. The NRC is permitted to make the number of copies of the information contained in these reports which are necessary for its internal use in connection with generic and plant-specific reviews and approvals as well as the issuance, denial, amendment, transfer, renewal, modification, suspension, revocation, or violation of a license, permit, order, or regulation subject to the requirements of 10 CFR 2.390 regarding restrictions on public disclosure to the extent such information has been identified as proprietary by Westinghouse, copyright protection notwithstanding. With respect to the non-proprietary versions of these reports, the NRC is permitted to make the number of copies beyond those necessary for its internal use which are necessary in order to have one copy available for public viewing in the appropriate docket files in the public document room in Washington, DC and in local public document rooms as may be required by NRC regulations if the number of copies submitted is insufficient for this purpose. Copies made by the NRC must include the copyright notice in all instances and the proprietary notice if the original was identified as proprietary.

PSEG Nuclear LLC

Letter for Transmittal to the NRC

The following paragraphs should be included in your letter to the NRC:

Enclosed is:

1. 1 copy of WCAP-16640-P, "Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet at Salem Unit 1," dated August 2006 (Proprietary)

2. 1 copy of WCAP-16640-NP, "Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet at Salem Unit 1," dated August 2006 (Non-proprietary)

Also enclosed is Westinghouse authorization letter CAW-06-2196 with accompanying affidavit, Proprietary Information Notice, and Copyright Notice.

As Item 1 contains information proprietary to Westinghouse Electric Company LLC, it is supported by an affidavit signed by Westinghouse, the owner of the information. The affidavit sets forth the basis on which the information may be withheld from public disclosure by the Commission and addresses with specificity the considerations listed in paragraph (b) (4) of Section 2.390 of the Commission's' regulations.

Accordingly, it is respectfully requested that the information which is proprietary to Westinghouse be withheld from public disclosure in accordance with 10 CFR Section 2.390 of the Commission's regulations.

Correspondence with respect to the copyright or proprietary aspects of the items listed above or the supporting Westinghouse affidavit should reference CAW-06-2196 and should be addressed to J. A. Gresham, Manager, Regulatory Compliance and Plant Licensing, Westinghouse Electric Company LLC, P.O. Box 355, Pittsburgh, Pennsylvania 15230-0355.

EVAL-06-58 SHC, "Significant Hazards Consideration (SHC), Salem Unit 1"

Note: On Page 18 of 21 of this report, a numerical typographical / transposition error was detected. The accident induced leakage multiplier was incorrectly stated as 0.187; the correct value is 0.179, as documented in WCAP-16640. This error has no impact on the analysis or the purpose of the report.

Customer Reference No(s).

Westinghouse Reference No(s).
EVAL-06-58 SHC

WESTINGHOUSE NUCLEAR SAFETY
SIGNIFICANT HAZARDS CONSIDERATION (SHC)

1) NUCLEAR PLANT(S): Salem Unit 1

2) SUBJECT: Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet

3) TECHNICAL SPECIFICATIONS CHANGED: 6.8.4.j Steam Generator (SG) Program, B3/4.4.5 Steam Generator Tube Integrity, 6.9.1.10 Steam Generator Tube Inspection Report and 6.9.1.5.b Reports

4) A written analysis of the significant hazards consideration, in accordance with the three factor test of 10 CFR 50.92, of a proposed license amendment to implement the subject change has been prepared and is attached. On the basis of the analysis the checklist below has been completed.

Will operation of the plant in accordance with the proposed amendment:

4.1) Yes__ No X Involve a significant increase in the probability or consequences of an accident previously evaluated;

4.2) Yes__ No X Create the possibility of a new or different kind of accident from any accident previously evaluated;

4.3) Yes__ No X Involve a significant reduction in a margin of safety.

5) REFERENCE DOCUMENT:

1. WCAP-16640-P, "Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet at Salem Unit 1", August 2006

6) Comments: None

7) APPROVAL LADDER:

7.1) Prepared by: G.W. Whiteman Date: 9/1/06

7.2) Reviewed by: R.A. Taylor Date: 9/1/06

Steam Generator Alternate Repair Criteria for Tube Portion
Within the Tubesheet for Salem Unit 1
10 CFR 50.92 Significant Hazards Consideration Evaluation

1.0 BACKGROUND

Indications of inner diameter (ID) cracking were reported based on the results from the nondestructive, eddy current examination of the steam generator (SG) tubes during the fall 2004 outage at the Catawba 2 nuclear power plant operated by the Duke Power Company. The tube indications at Catawba were reported about 7.6 inches from the top of the tubesheet in one tube, and just above the tube-to-tubesheet welds in a region of the tube known as the tack expansion (TE) in several other tubes. Finally, indications were also reported in the tube end welds (TEWs), also known as tube-to-tubesheet welds, joining the tube to the tubesheet. The Catawba 2 plant has Westinghouse designed, Model D5 SGs fabricated with Alloy 600TT (thermally treated) tubes. There is the potential for additional tube indications similar to those already reported at Catawba 2 within the tubesheet region to be reported during future inspections.

It was subsequently noted that an indication was reported in SG tubes at the Vogtle Unit 1 plant operated by the Southern Nuclear Operating Company. The Vogtle SGs are of the Westinghouse Model F design with slightly smaller, diameter and thickness, A600TT tubes.

Note: No indications of this type were found during the planned inspections of the Braidwood 2 SG (Model D5 SGs) tubes in April 2005, a somewhat similar inspection of the tubes in two SGs at Wolf Creek (Model F SGs) in April 2005, or an inspection of the tubes at Comanche Peak 2 (Model D5 SGs) in the spring of 2005. Also, no indications of this type were found during similar inspections at Byron 2 (Model D5 SGs) and Vogtle 2 (Model F SGs) in the fall of 2005. Additionally, 1054 bulges were examined in 2 SGs at Millstone Unit 3 in the fall of 2005 with no crack indications identified.

The SGs for all four Model D5 plant sites were fabricated in the 1978 to 1980 timeframe using similar manufacturing processes with a few exceptions. For example, the fabrication technique used for the installation of the SG tubes at Braidwood 2 would be expected to lead to a much lower likelihood for crack-like indications to be present in the region known as the tack expansion relative to Catawba 2 because a lower stress urethane expansion process for effecting the tack expansions was adopted prior to the time of the fabrication of the Braidwood 2 SGs. The tack expansions in the steam generator tubes at Salem Unit 1 were completed by a urethane expansion process (similar to Braidwood Unit 2) as they were shipped in 1983.

The findings in the Catawba 2 and Vogtle 1 SG tubes present three distinct issues with regard to future inspections of Alloy 600TT SG tubes which have been hydraulically expanded into the tubesheet:

1) indications in internal bulges within the tubesheet,
2) indications at the elevation of the tack expansion transition, and
3) indications in the tube-to-tubesheet welds, including some extending into the tube.

2.0 DESCRIPTION

As a result of the discussion in Section 1.0, an evaluation has been performed for Salem Unit 1 that considers the requirements of the ASME Code, Regulatory Guides, NRC Generic Letters, NRC Information Notices, the Code of Federal Regulations, NEI 97-06, and additional industry requirements (Reference 1). The conclusion of the technical evaluation is that:

1) the structural integrity of the primary-to-secondary pressure boundary is unaffected by tube degradation of any magnitude below a tube location-specific depth ranging from 2.25 to 7.05 inches, designated as H* (considering both hot and cold leg H* values), and,
2) that the accident condition leak rate can be bounded by twice the normal operation leak rate from degradation below a distance from the top of the tubesheet known as the B* distance of 6.83 inches.

These results follow from finite element analyses demonstrating that the tube-to-tubesheet hydraulic joints make it extremely unlikely that any operating or faulted condition loads are transmitted below the H* elevation, and the contact leak rate resistance increases below a certain elevation within the tubesheet. Internal tube bulges within the tubesheet are created in a number of tubes as an artifact of the manufacturing process. The analyses results support a license amendment to eliminate inspection of the region of the tube below a bounding H* or B* distance from the top of the tubesheet. For the Salem Unit 1 SGs, a conservative distance of 8.0 inches from the top of the tubesheet is justified as the inspection distance. This distance takes into account variation in the location of the bottom of the hydraulic expansion transition from the top of the tubesheet, NDE uncertainty in determining the location of the tip of axial cracks from the bottom of the expansion transition (BET), and NDE uncertainty in determining the H* length. Such an amendment is interpreted to constitute a redefinition of the primary-to-secondary pressure boundary relative to the original design of the SG and requires the approval of the NRC staff through a license amendment.

A similar type of Technical Specification change was approved, on a one-time basis, to limit inspections of the Braidwood 2 and Wolf Creek SGs during the spring 2005 inspection campaigns to a distance 17 inches from the top of the tubesheet. Subsequent approvals were also obtained for use at Byron 2 and Vogtle 2 in their fall 2005 inspection campaigns for this same inspection distance.

The development of the H* criteria involved consideration of the performance criteria for the operation of the SG tubes as delineated in NEI 97-06, Revision 2. The bases for the performance criteria are the demonstration of both structural and leakage integrity during normal operation and postulated accident conditions. The structural model was based on standard analysis techniques and finite element models as used for the original design of the SGs and documented in numerous submittals for the application of criteria to deal with tube indications within the tubesheet of other models of Westinghouse designed SGs with tube-to-tubesheet joints fabricated by other techniques, e.g., explosive expansion.

All full depth expanded tube-to-tubesheet joints in Westinghouse-designed SGs have a residual radial preload between the tube and the tubesheet. Early vintage SGs involved hard rolling which resulted in the largest magnitude of the residual interface pressure. Hard rolling was replaced by explosive expansion which resulted in a reduced magnitude of the residual interface pressure. Finally, hydraulic expansion replaced explosive expansion for the installation of SG tubes, resulting in a further reduction in the residual interface pressure. In general, it was found that the leak rate through the joints in hard rolled tubes, if any, is insignificant. Testing demonstrated that the leak rate resistance of explosively expanded tubes was not as great and prediction methods based on empirical data to support theoretical models were developed to deal with the potential for leakage. The same approach was followed to develop a prediction methodology for hydraulically expanded tubes. However, the model has been under review by Westinghouse since its inception, with the intent of verifying its accuracy because it involved analytically combining the results from independent tests of leak rate through cracks with the leak rate through the tube-to-tubesheet crevice. The review of the H* model for leak rate could be time consuming since it's accuracy has already been questioned by the NRC staff and identified as an issue. An alternative approach was developed for application at Salem Unit 1 from engineering expectations of the relative leak rate between normal operation and postulated accident conditions based on a first principles engineering approach.

PSEG Nuclear LLC plans to implement the following repair criteria and acceptance criteria.

- Any type or combination of tube degradation below 8 inches from the top of the tubesheet on the hot or cold leg side may be left in service.

- Degradation in tubes less than or equal to 8 inches from the top of the tubesheet on the hot leg or cold leg side must be plugged.

3.0 PROPOSED TECHNICAL SPECIFICATION CHANGES

Specifically, the proposed changes will revise the following Technical Specifications on a permanent basis:

TS B3/4.4.5	Steam Generator (SG) Tube Integrity
TS 6.8.4.i	Steam Generator (SG) Program
TS 6.9.1.10	Steam Generator Tube Inspection Report
TS 6.9.1.5.b	Reports[1]

The proposed changes clearly delineate the scope of the steam generator inspection required in the tube joint region of the Salem Unit 1 steam generators. The proposed Technical Specification changes are described below:

TS B 3/4.4.5 Steam Generator Tube Integrity

TS B 3/4.4.5 Steam Generator Tube Integrity currently states:

In the context of this Specification, a steam generator tube is defined as the entire length of tube, including the tube wall, between the tube-to-tubesheet weld at the tube inlet and the tube-to-tubesheet weld at the tube outlet. The tube-to-tubesheet weld is not considered part of the tube.

This criterion will be revised as follows as noted in bold type:

In the context of this Specification, a steam generator tube is defined as the ~~entire~~ length of tube, including the tube wall, ~~between the tube-to-tubesheet weld at the tube inlet and the tube-to-tubesheet weld at the tube outlet~~ **beginning 8 inches from the top of the tubesheet on the tube hot leg side and extending to 8 inches below the top of the tubesheet on the tube cold leg side.** The tube-to-tubesheet weld is not considered part of the tube.

[1] The change to TS 6.9.1.5.b is not a direct consequence of the B* criteria, but is a related correction to the TS that has been superseded by Salem Unit 1 Amendment 268, dated October 1, 2005.

TS 6.8.4.i Steam Generator (SG) Program

TS 6.8.4.1 c) currently states:

c. "Provisions for SG tube repair criteria. Tubes found by inservice inspection to contain flaws with a depth equal to or exceeding 40% of the nominal tube wall thickness shall be plugged."

This criterion will be revised as follows as noted in bold type:

c. "Provisions for SG tube repair criteria. Tubes found by inservice inspection to contain flaws with a depth equal to or exceeding 40% of the nominal tube wall thickness shall be plugged. **In lieu of the 40% of the nominal wall thickness repair criteria, the portion of the tube within the tubesheet of the inspected SGs shall be plugged in accordance with the alternate repair criteria defined in WCAP-16640-P, "Steam Generator Alternate Repair Criteria for the Tube Portion within the Tubesheet at Salem Unit 1." Degradation found in the portion of the tube below 8 inches from the top of the tubesheet does not require plugging. Degradation identified in the portion of the tube from the top of the tubesheet to 8 inches below the top of the tubesheet shall be plugged on detection.**

TS 6.8.4.i d) currently states:

d. "Provisions for SG tube inspections. Periodic SG tube inspections shall be performed. The number and portions of the tubes inspected and methods of inspection shall be performed with the objective of detecting flaws of any type (e.g., volumetric flaws, axial and circumferential cracks) that may be present along the length of the tube, from the tube-to-tubesheet weld at the tube inlet to the tube-to-tubesheet weld at the tube outlet, and that may satisfy the applicable repair criteria. The tube-to-tubesheet weld is not part of the tube....."

This criterion will be revised as follows as noted in strikethrough text and bold type:

d. "Provisions for SG tube inspections. Periodic SG tube inspections shall be performed. The number and portions of the tubes inspected and methods of inspection shall be performed with the objective of detecting flaws of any type (e.g., volumetric flaws, axial and circumferential cracks) that may be present along the length of the tube **beginning 8 inches from the top of the tubesheet on the tube hot leg side to 8 inches below the top of the tubesheet on the tube cold leg side as defined in WCAP-16640-P** ~~from the tube-to-tubesheet weld at the tube inlet to the tube-to-tubesheet weld at the tube outlet, and that may satisfy the applicable repair criteria~~. The tube-to-tubesheet weld is not part of the tube....."

TS 6.9.1.10 Steam Generator Tube Inspection Report

TS 6.9.1.10 currently states:

A report shall be submitted within 180 days after the initial entry into HOT SHUTDOWN following completion of an inspection performed in accordance with the Specification 6.8.4.i, "Steam Generator (SG) Program." The report shall include:

a. The scope of inspections performed on each SG,
b. Active degradation mechanisms found,
c. Nondestructive examination techniques utilized for each degradation mechanism,
d. Location, orientation (if linear), and measured sizes (if available) of service induced indications,
e. Number of tubes plugged during the inspection outage for each active degradation mechanism,
f. Total number and percentage of tubes plugged to date, and
g. The results of condition monitoring, including results of tube pulls and in-situ testing

This criterion will be revised as follows as noted in bold type:

A report shall be submitted within 180 days after the initial entry into HOT SHUTDOWN following completion of an inspection performed in accordance with the Specification 6.8.4.i, "Steam Generator (SG) Program." The report shall include:

a. The scope of inspections performed on each SG,
b. Active degradation mechanisms found,
c. Nondestructive examination techniques utilized for each degradation mechanism,
d. Location, orientation (if linear), and measured sizes (if available) of service induced indications,
e. Number of tubes plugged during the inspection outage for each active degradation mechanism,
f. **The number of indications detected in the upper 8 inches of the tubesheet thickness along with their location, measured size, orientation, and whether the indication initiated on the primary or secondary side.**
g. **The operational primary to secondary leakage rate observed in each steam generator during the cycle preceding the inspection which is the subject of the report and the calculated accident leakage rate for each steam generator from the lowermost 13 inches of tubing (the tubesheet is nominally 21.03 inches thick) for the most limiting accident. If the calculated leak rate is less than 2 times the total observed operational leakage rate, the 180 day report should describe how it is determined.**
h. Total number and percentage of tubes plugged to date, and

i. The results of condition monitoring, including results of tube pulls and in-situ testing

TS 6.9.1.5.b currently states:

[Report required on an annual basis shall include:] The complete results of steam generator tube inservice inspections performed during the report period (reference Specification 4.4.5.5.b).

TS 6.9.1.5.b will be deleted; this reporting requirement was superceded by Salem Unit 1 Amendment 268, dated October 14, 2005, which implemented TSTF-449. Amendment 268 deleted TS 4.4.5.5.b and added TS 6.9.1.10 for SG inspection reporting.

4.0 TECHNICAL ANALYSIS

Evaluations were performed in Reference 1 to assess the need for special purpose NDE probe examinations of the SG tubes region within the tubesheet at the Salem Unit 1 power plant. The conclusions from the evaluation are that any such inspections can be limited in each SG to a depth of 8 inches from the top of the tubesheet. Tube inspection and plugging will be completed as defined in the plant Technical Specifications.

It is noted that the above inspection does not include the region of the tube referred to as the tack expansion, the tack expansion transition, and the tube end weld location. It is concluded that there is no need to inspect either the tack expansion, its transition, or the tube-to tubesheet welds for degradation. The results from the evaluations performed as described herein demonstrate that the inspection of the weld or the tube near the tube-to-tubesheet weld is not necessary to demonstrate structural adequacy of the SG during normal operation or during postulated faulted conditions or compliance with leak rate limits during postulated faulted events.

4.1 Structural Analysis

The tube end weld was originally designed as a pressure boundary structural element in accordance with the requirements of Section III of the ASME (American Society of Mechanical Engineers) Boiler and Pressure Vessel Code. The construction code for the Salem Unit 1 SGs is the 1971 Edition through the Summer 1973 Addenda. There were no strength considerations made with regard to the expansion joint between the tube and the tubesheet, including the tack expansion regardless of whether it was achieved by rolling or Poisson expansion of a urethane plug.

An extensive empirical and analytical evaluation of the structural capability of the as-installed tube-to-tubesheet joints based on considering the weld to be absent was performed specifically for the Salem Unit 1 SGs and the results are reported in Reference 1. Typical Model F hydraulic expansion joints with lengths comparable to those being considered for limiting RPC examination were tested for pullout resistance strength at

temperatures ranging from 70 to 600°F. The results of the tests were coupled with results from finite element evaluations of the effects of temperature and primary-to-secondary pressure on the tube-to-tubesheet interface loads. The combined results were used to demonstrate that engagement lengths of 2.25 to 7.05 inches (herein referred to as the H* distance) were sufficient to equilibrate the axial loads resulting from consideration of three times the normal operating pressure difference and 1.4 times the limiting accident condition pressure difference. The required engagement length is a function of tube location (i.e., row and column numbers), and decreases away from the center of the SG. The tubesheet bows upward from the primary-to-secondary pressure difference and results in the tube holes becoming dilated above the neutral plane of the tubesheet, a little below the center because of the effect of the tensile membrane stress from the pressure loading. The amount of dilation is a maximum very near the center of the tubesheet and diminishes radially outward. Moreover, the tube-to-tubesheet joint becomes tighter below the neutral axis and is a maximum at the bottom of the tubesheet. In conclusion, the need for the weld is obviated by the interference fit between the tube and the tubesheet. Axial loads are not transmitted to the portion of the tube below the structural distance during operation or faulted conditions, with factors of safety of at least 3 and 1.4 respectively. Inspection of the tube below the structural distance including the tube-to-tubesheet weld is not technically necessary. If the expansion joint were not present, there would no effect on the strength of the weld from axial cracks, and tubes with circumferential cracks up to about 180° in azimuthal extent by 100% deep would have sufficient strength to meet the nominal ASME Code structural requirements, based on the margins of safety reported in Reference 1 and the requirements of RG 1.121.

4.2 Leak Rate Analysis of Cracked Tube-to Tubesheet Joints

A discussion of the leak rate expectations from axial and circumferential cracking confined to the tube-to-tubesheet joint region, including the tack expansion region, the tube-to-tubesheet welds and areas where degradation may eventually occur due to bulges and over expansions within the tube is provided below.

From an engineering expectation standpoint, if there is no meaningful primary-to-secondary leakage during normal operation, there should likewise be no meaningful leakage during postulated accident conditions from indications located below the mid-plane of the tubesheet. The rationale for this is based on consideration of the deflection of the tubesheet with attendant dilation and diminution (expansion and contraction) of the tubesheet holes. In effect, the leakage flow area depends on the contact pressure between the tube and tubesheet and would be expected to decrease during postulated accident conditions below some distance from the top of the tubesheet. The primary-to-secondary pressure difference during normal operation is on the order of 1200 to 1500 psid, while that during a postulated accident, e.g., steam line and feed line break, is on the order of 2560 to 2650 psid.[1] Above the neutral plane of the tubesheet the tube holes tend to experience a dilation due to pressure induced bow of the tubesheet. This means that the contact pressure between the tubes and the tubesheet would diminish above the neutral

[1]The differential pressure could be on the order of 2405 psi if it is demonstrated that the power operated relief valves will be functional.

plane in the central region of the tubesheet at the same time as the driving potential would increase. Therefore, if there was leakage through the tube-to-tubesheet crevice during normal operation from a through-wall tube indication, that leak rate could be expected to increase during postulated accident conditions. Based on early NRC staff queries regarding the leak rate modeling code associated with calculating the expected leak rate, it was expected that efforts to license criteria based on estimating the actual leak rate as a function of the contact pressure during faulted conditions on a generic basis would be problematic.

As noted, the tube holes diminish in size below the neutral plane of the tubesheet because of the upward bending and the contact pressure between the tube and the tubesheet increases. When the differential pressure increases during a postulated faulted event the increased bow of the tubesheet leads to an increase in the tube-to-tubesheet contact pressure, increasing the resistance to flow. Thus, while the dilation of the tube holes above the neutral plane of the tubesheet presents additional analytical problems in estimating the leak rate for indications above the neutral plane, the diminution of the holes below the neutral plane presents definitive statements to be made with regard to the trend of the leak rate, hence, the bellwether principle. Independent consideration of the effect of the tube-to-tubesheet contact pressure leads to similar conclusions with regard to the opening area of the cracks in the tubes, thus further restricting the leak rate beyond that through the interface between the tube and the tubesheet.

In order to accept the concept of normal operation being a bellwether for the postulated accident leak rate for indications above the neutral plane of the tubesheet, the change in leak rate had to be quantified using a somewhat complex, physically sound model of the thermal-hydraulics of the leak rate phenomenon. This is not necessarily the case for cracks considered to be present below the neutral plane of the tubesheet. This is because a diminution of the holes takes place during postulated accident conditions below the neutral plane relative to normal operation. For example, at a radius of approximately 30.19 inches from the center of the SG at the bottom of the tubesheet, the contact pressure during normal operation is calculated to be 2886.85 to 2759.66 psi[2], while the contact pressure during a postulated steam line break would be on the order of 4501.46 psi near the bottom of the tubesheet, The analytical model for the flow through the crevice, the Darcy equation for flow through porous media, indicates that flow would be expected to be proportional to the differential pressure. Thus, a doubling of the leak rate could be predicted if the change in contact pressure between the tube and the tubesheet were ignored. Examination of the nominal correlation in Reference 1 indicates that the resistance to flow (the loss coefficient) would increase during a postulated SLB event.

The leak rate from a crack located within the tubesheet is governed by the crack opening area, the resistance to flow through the crack, and the resistance to flow provided by the tube-to-tubesheet joint. The path through the tube-to-tubesheet joint is also frequently referred to as a crevice, but is not to be confused with the crevice left at the top of the tubesheet from the expansion process. The presence of the joint makes the flow from

[2] The change occurs as a result of considering various hot and cold leg operating temperatures.

cracks within the tubesheet much different from the flow to be expected from cracks outside of the tubesheet. The tubesheet prevents outward deflection of the flanks of cracks, a more significant effect for axial than for circumferential cracks, which is a significant contributor to the opening area presented to the flow. In addition, the restriction provided by the tubesheet greatly restrains crack opening in the direction perpendicular to the flanks regardless of the orientation of the cracks. The net effect is a large, almost complete restriction of the leak rate when the tube cracks are within the tubesheet.

The leak path through the crack and the crevice is very tortuous. The flow must go through many turns within the crack in order to pass through the tube wall, even though the tube wall thickness is relatively small. The flow within the crevice must constantly change direction in order to follow a path that is formed between the points of hard contact between the tube and the tubesheet as a result of the differential thermal expansion and the internal pressure in the tube. There is both mechanical dispersion and molecular diffusion taking place. The net result is that the flow is best described as primary-to-secondary weepage. At its base, the expression used to predict the leak rate from tube cracks through the tube-to-tubesheet crevice is the Darcy expression for flow rate, Q, through porous media, i.e.,

$$Q = \frac{1}{K\mu}\frac{dP}{dz}$$

where μ is the viscosity of the fluid, P is the driving pressure, z is the physical dimension in the direction of the flow, and K is the "loss coefficient" which can also be termed the flow resistance if the other terms are taken together as the driving potential. The loss coefficient is found from a series of experimental tests involving the geometry of the particular tube-to-tubesheet crevice being analyzed, including factors such as surface finish, and then applied to the cracked tube situation.

If the leak rate during normal operation was 0.05 gpm (75 gpd), the postulated accident condition leak rate would be on the order of 0.1 gpm if only the change in differential pressure were considered; however, the estimate would be reduced when the increase in contact pressure between the tube and the tubesheet was included during a postulated steam line break event. An examination of the contact pressures as a function of depth in the tubesheet from the finite element analyses of the tubesheet shows that the bellwether principle applies to a significant extent to all indications below the neutral plane of the tubesheet. At the neutral plane of the tubesheet, the increase in contact pressure shown is more on the order of 33% relative to that during normal operation for all tubes regardless of radius. Still, the fact that the contact pressure increases means that the leak rate would be expected to be bounded by a factor of two relative to normal operation.

A comparison of the contact pressure during postulated SLB conditions relative to that during normal operation (NOp) is also provided for depths of 16.9, 10.515 and 6.0 inches below the top of the tubesheet. The observations are discussed in the following.

- At the bottom of the tubesheet, the contact pressure increases by 1700 psi near the center of the tubesheet and exhibits no change at a radius of about 55 inches.

- At 16.9 inches below the top of the tubesheet (a little over 4.1 inches from the bottom) the tubesheet the contact pressure increases by about 1200 psi at the center to a minimum of about 200 psi at a radius of 57 inches. The contact pressure during a SLB is everywhere greater than that during NOp. The influence of the channelhead and shell at the periphery causes the deformation to become non-uniform near the periphery.
- At roughly the neutral surface, about 10.5 inches from the top of the tubesheet, the contact pressure during SLB is uniformly greater than that during normal operation by about 500 psi.
- At a depth of 6.00 inches from the TTS, the contact pressure stays constant near the center of the TS and increases by about 500 psi near the periphery.

B* is the length of engagement in the tubesheet needed for the leak rate during a postulated steam line break (SLB) event to be bounded by a specified multiple of the leak rate during normal operation (NOp). The rationale for the determination of B* is that there are changes during a SLB relative to NOp that lead to the expectation of an increase in the leak rate and other changes that lead to the expectation of a higher resistance to leakage. The determination of B* is based on analyzing the contributing factors and making an estimate of the change in leak rate that would be expected. The factors that lead to an expectation of an increase in the leak rate are as follows:

1. An increase in the primary-to-secondary differential pressure induced force on the water inside a postulated tube crack and the tube-to-tubesheet interface. For Salem Unit 1 this is a factor ranging from 1.70 to 1.79.

2. A decrease in the tube-to-tubesheet contact pressure above the neutral plane of the tubesheet resulting from dilation of the tubesheet holes in response to an increase in the bending deformation from the primary-to-secondary pressure difference increase. This does not apply to the periphery of the tubesheet where the opposite effect occurs.

3. A decrease in the tube-to-tubesheet contact pressure associated with the higher coefficient of thermal expansion of the tube material relative to that of the tubesheet if the temperature of the tubesheet decreases.

The factors that lead to an expectation of a decrease in the leak rate are:

1. The increase in primary pressure within the tube expands the tube into tighter contact with the tubesheet, resulting in an increase of the resistance of the material interface to flow between the tube and the tubesheet.

2. An increase in the tube-to-tubesheet contact pressure below the neutral plane of the tubesheet resulting from diminution of the tubesheet holes in response to the increase in the bending deformation from the primary-to-secondary pressure difference increase. Again, the effect is opposite for most tubes on the periphery of the tubesheet.

3. An increase in the resistance to flow associated with an increase in the viscosity of the water in the crevice if the temperature of the tubesheet decreases.

The basis for the determination of B* is the consideration of each of the above effects using results from finite element analyses of the tubesheet and results from leak rate testing of the tube-to-tubesheet interface.

A natural question regarding the development and application of the B* criterion is whether or not numerical studies were performed to verify that the reduction in leak rate resistance above the neutral surface of the tubesheet associated with tubesheet bowing was adequately bounded by the increase in resistance below the neutral surface. The following discussion is intended to provide technical insight into the behavior of the leak rate from throughwall tube indications within the tubesheet by presenting:

1) the theoretical detail that is the basis for the observations from the test data and extrapolation of the test data for leak rate as function of joint length as expressed as the flow loss coefficient, and,

2) the explanation as to why the leak rate at normal operating conditions provides a bellwether for and can be used to establish a bounding value for the leak rate during steam line break conditions.

For most of the tube locations in the tubesheet the bow is convex upwards, like a dome. The tube-to-tubesheet contact pressure is an increasing linear function of the depth from the top of the tubesheet, thus, for any specified location within the tubesheet the contact pressure increases below and decreases above that location. The resistance to leakage through the tube-to-tubesheet interface is an increasing function of the contact pressure between the tube and the tubesheet. The bellwether principle is based on considering the leak rate during a postulated steam line break (SLB) event relative to that during normal operating conditions (NOp). The primary-to-secondary differential pressure during a SLB event is greater than that during NOp so that bowing of the tubesheet increases with an associated change in the slope of the contact pressure versus depth relation as a function of tube location. For all tubes, except for the small percentage of tubes that are located on the periphery, the slope increases. For tubes on the periphery the slope increases in an absolute sense since there is an inflection point near the periphery. Regardless, the evaluation applies because increasing the contact pressure has a greater influence on the leak rate than decreasing the contact pressure.

Numerical studies were not initially performed because the subject was considered to be adequately addressed based on a qualitative evaluation using first principles considerations as follows:

1) In the limiting case of no dependence of the leak rate loss coefficient, i.e., the resistance per unit length, on the contact pressure, the leak rate during NOp and postulated SLB would be a function of the length of the crevice and pressure difference only. Using the Darcy equation, the leak rate is a direct function of the differential pressure and the inverse of the crevice length. Since the length remains the same and the driving pressure increases by a factor of about 2, that is, 1300 psi to 2560 psi, the leak rate change is bounded by a similar factor. Any other theoretical dependence of leak rate on pressure difference, e.g., the square root of the pressure difference à la the Bernoulli equation, results in a reduction of the bounding factor relative to the result obtained using the Darcy equation.

2) The test data have demonstrated that the resistance per unit length is a monotonically increasing, non-linear function of the contact pressure with a positive second derivative. The deflection of the TS in combination with the increase in internal pressure results in the change in the contact pressure being zero between NOp and SLB at some depth below the TTS that is above the neutral surface of the tubesheet. The net contact pressure decreases above and increases below that depth, which is a function of location within the tube bundle. Using this elevation as a reference, the increase in resistance per unit length below the zero-change location must always be more than the absolute value of the decrease in resistance per unit length above the zero-change elevation. Thus, the average resistance in going from NOp to SLB must increase and the average leak rate must decrease. This is independent of the individual leak rates involved and only depends on the trend.

The equation that is solved for flow through a crevice is Darcy's model for flow through a porous media, that is, the volumetric flow, Q, is a function of the differential driving pressure, ΔP, and the respective inverse values of the viscosity, μ, the loss coefficient, K, and the length of the path, L, as,

$$Q = \frac{1}{\mu K}\frac{\Delta P}{L}. \tag{2-1}$$

The results from multiple leak rate testing programs indicate that:

$$P_c = b_0 + b_1 L \tag{2-2}$$

and that the logarithm of the loss coefficient is a linear function of the contact pressure, i.e.,

$$\ln K = a_0 + a_1 P_c, \tag{2-3}$$

where the coefficients, a_0 and a_1 of the linear relation are found from a regression analysis of the test data; both coefficients are greater than zero. Simply put, the loss coefficient is

greater than zero at the point where the contact pressure is zero and the loss coefficient increases with increasing contact pressure. Thus,

$$K = e^{a_0 + a_1 P_c}, \tag{2-4}$$

and the loss coefficient is an exponential function of the contact pressure. Combining Equation 2-4 for the loss coefficient as a function of the contact pressure with Equation 2-2 for the contact pressure as a function of length yields,

$$K = e^{a_0 + a_1 (b_0 + b_1 L)} = e^{c_0 + c_1 L} \tag{2-5}$$

where L is reckoned downward from the lower of the top of the tubesheet or the bottom of the expansion transition and the joined coefficients are given by $c_0 = a_0 + a_1 b_0$ and $c_1 = a_1 b_1$. Away from the periphery of the tubesheet, b_1 is greater than zero, hence c_1 is also greater than zero and the loss coefficient increases with depth into the tubesheet. Alternatively, the relation also means that near the periphery of the tubesheet the resistance to flow increases above any depth when the tubesheet bows upward. Since the B* distance into the tubesheet is based on finding the depth for which the resistance to leak during SLB is the same as that during NOp, the meaningful radial region of the tubesheet is away from the periphery, that is, where the resistance to leakage decreases near the top of the tubesheet. Another point to note from the above expression is that in the region of interest the second derivative of the loss coefficient with respect to depth is positive. this means that the resistance per unit length is always increasing with depth into the tubesheet. One consequence of the relation is that the decrease in resistance for a specified distance above any reference point is balanced by the increase in resistance over a shorter distance below that reverence point. The B* distance is designated by L_B in the following equations and is the depth at which the resistance to leak during SLB is the same as that during NOp. Note that the product of the viscosity and the loss coefficient is the resistance per unit length for any location in the tubesheet. The resistance to leak, R, as a function of the viscosity, μ, average loss coefficient, $\overline{K}$, and length of the leak path from some uppermost location, L_0, to L_B for any condition is given by,

$$R = \mu \overline{K} (L_B - L_0) = \mu \int_{L_0}^{L_B} e^{c_0 + c_1 L} dL. \tag{2-6}$$

The limits of the integration define the range over which there is a contact pressure between the tube and the tubesheet that is greater than zero, i.e., ignoring any resistance to flow above that elevation. The lower limit is the lower of the TTS, the BET, or the point where the contact pressure is zero. Carrying out the integration,

$$R = \mu \frac{e^{c_0}}{c_1} \left[e^{c_1 L_B} - e^{c_1 L_0} \right]. \tag{2-7}$$

The equation can be used directly when the point of zero contact pressure between the tube and the tubesheet is at or below the TTS or BET, whichever is lower.

In order to account for the condition wherein L_0 is < 0, i.e., at or above the TTS or BET, whichever is lower, the equation is written as,

$$R = \mu \frac{e^{c_0}}{c_1}\left[e^{c_1 L_B} - \text{if}\left(L_0 > 0, e^{c_1 L_0}, 1\right)\right]. \tag{2-8}$$

Here, the first argument of the "if" statement is the condition to be tested, the second argument is the value used if the condition is true, and the third argument is used if the condition is false, that is, when zero contact pressure is predicted above the TTS or BET. For normal operation the resistance to leakage is given by,

$$R_N = \mu_N \frac{e^{c_{0N}}}{c_{1N}}\left[e^{c_{1N} L_B} - \text{if}\left(L_{0N} > 0, e^{c_{1N} L_{0N}}, 1\right)\right], \tag{2-9}$$

and for SLB by,

$$R_S = \mu_S \frac{e^{c_{0S}}}{c_{1S}}\left[e^{c_{1S} L_B} - \text{if}\left(L_{0S} > 0, e^{c_{1S} L_{0S}}, 1\right)\right]. \tag{2-10}$$

The B* distance is such that the resistance during SLB is the same as that during NOp, limiting the leak rate to be no more than a factor of two times that during normal operation, thus the solution is obtained for the value of L_B that makes $R_S = R_N$.

For the Salem Unit 1 SGs the value of B* varies from a maximum of 6.83 inches at a radius of 2 inches, about row 1 at the center of the bundle, to less than 5 inches at a radius of 20 inches. Thereafter, the integrated leak resistance is always greater during a SLB event than during NOp. The top of the tubes at the extreme periphery of the tubesheet are in compression during NOp because of contraction of the tube holes due to convex downward bending. The level of compression increases during a SLB event because the magnitude of the convex bending increases. Thus, any leak rate during NOp would bound the leak rate during SLB. While the driving pressure would increase by a factor of up to 2, the contact pressure between the tube and the tubesheet would increase toward the top of the tubesheet.

5.0 REGULATORY ANALYSIS

SG tube inspection and repair limits are specified in Section 6.8.4.i, "Steam Generator (SG) Program of the Salem Unit 1 Technical Specifications. The current TS require that flawed tubes be repaired if the depths of the flaws are greater than or equal to 40 percent through wall. The TS repair limits ensure that tubes accepted for continued service will retain adequate structural and leakage integrity during normal operating, transient, and postulated accident conditions, consistent with General Design Criteria (GDC) 14, 15, 30, 31, and 32 of 10 CFR 50, Appendix A. Structural integrity refers to maintaining adequate margins against gross failure, rupture, and collapse of the steam generator tubing. Leakage integrity refers to limiting primary to secondary leakage to within acceptable limits.

H* addresses the structural requirements. H* defines the minimum length of engagement required for hydraulically expanded tubes to prevent tube pullout from the tubesheet under limiting accident conditions. The principal loads acting to pull a tube from the tubesheet are end-cap loads resulting from the primary to secondary pressure differentials. H* varies with radial position from the tubesheet centerline due to tubesheet bow resulting from the primary-to-secondary pressure differential. The bow increases during accident conditions due to a greater pressure differential across the tubesheet. Increased tubesheet bow causes tube-hole bore dilation above the neutral axis resulting in reduced interface loads between the tube and the tubesheet. Tubesheet bending varies with the radial distance from the centerline of the tubesheet as dictated by the structural constraints of the tubesheet, e.g., shell and support ring on the OD and divider plate at the centerline.

B* addresses leakage requirements. As defined in this document, B* is the distance from the top of the tubesheet where the leakage flow resistance at SLB conditions equals the leakage flow resistance at normal operating conditions. This definition of B* is useful in that the accident leakage will be equal to the ratio of the accident pressure differential to the normal operating pressure differential times the normal operating leakage. If effect, the normal operating leakage becomes a "bellwether" for the accident leakage; therefore, if normal operating leakage is within acceptable limits, accident induced leakage will also be within acceptable limits.

A relocation of the pressure boundary to the deeper of the H* or B* values is acceptable from both a structural and leak rate considerations depending on the relative allowable leak rate during accident conditions. The prior conclusions rely on the inherent strength and leak rate resistance of the hydraulically expanded tube-to-tubesheet joint, a feature which was not considered or permitted to be considered for the original design of the SG.

Using the maximum H* depth of 7.05 inches, a governing inspection depth of 8 inches is established which would be sufficient to address all hot and cold leg considerations for both structural and leak rate integrity. As described in Reference 1, this inspection distance includes uncertainty in the location of the bottom of the BET (0.30 inches

maximum) and NDE uncertainty in determining the H* length of non-degraded tubing using the rotating probe (0.12 inches), and the NDE uncertainty associated with the locating the distance of an axial crack from the BET (0.28 inches) [6.75 inches + 0.30 inches + 0.12 inches + 0.28 inches < 8.0 inches).

The Technical Specification allowable normal operating leak rate for Salem Unit 1 is 150 gpd (0.1 gpm) and the allowable accident induced leak rate is 500 gpd (0.35gpm) total in the affected SG. The SLB differential pressure is a factor of 1.79 greater than the NOp differential pressure at Salem Unit 1. Therefore, if the current NOp leakage is at its limiting value, 0.1 gpm (150 gpd), the accident induced leakage will not exceed 0.187 gpm (268.5 gpd) if the bounding values of H* is applied. Recall that this does not include consideration of the reduction in flow associated with an increase in viscosity as the temperature decreases.

In conclusion, based on the considerations discussed above, (1) there is reasonable assurance that the health and safety of the public will not be endangered by operation in the proposed manner, (2) such activities will be conducted in compliance with the Commission's regulations, and (3) the issuance of the amendment will not be inimical to the common defense and security or to the health and safety of the public.

6.0 NO SIGNIFICANT HAZARDS CONSIDERATION

PSEG Nuclear has evaluated the no significant hazards considerations involved with the proposed amendment, focusing on the three standards set forth in 10 CFR 50.92(c):

"The commission may make a final determination, pursuant to the procedures in paragraph 50.91, that a proposed amendment to an operating license for a facility licensed under paragraph 50.21(b) or paragraph 50.22 or for a testing facility involves no significant hazards considerations, if operation of the facility in accordance with the proposed amendment would not:

(1) Involve a significant increase in the probability or consequences of an accident previously evaluated; or

(2) Create the possibility of a new or different kind of accident from any accident previously evaluated; or

(3) Involve a significant reduction in a margin of safety."

The following evaluation is provided for the no significant hazards considerations.

1. Does the change involve a significant increase in the probability or consequences of an accident previously evaluated?

Of the accidents previously evaluated, the proposed changes only affect the steam generator tube rupture (SGTR) event evaluation and the postulated steam line break (SLB) accident evaluation. Loss-of-coolant accident (LOCA) conditions cause a compressive axial load to act on the tube. Therefore, since the LOCA tends to force the tube into the tubesheet rather than pull it out, it is not a factor in this amendment request. Another faulted load consideration is a safe shutdown earthquake (SSE); however, the seismic analysis of Model F steam generators has shown that axial loading of the tubes is negligible during an SSE.

At normal operating pressures, leakage from primary water stress corrosion cracking (PWSCC) below 8 inches from the top of the tubesheet is limited by both the tube-to-tubesheet crevice and the limited crack opening permitted by the tubesheet constraint. Consequently, negligible normal operating leakage is expected from cracks within the tubesheet region.

For the SGTR event, the required structural margins of the steam generator tubes will be maintained by the presence of the tubesheet. Tube rupture is precluded for cracks in the hydraulic expansion region due to the constraint provided by the tubesheet. Therefore, the performance criteria of NEI 97-06, Rev. 2, "Steam Generator Program Guidelines" and the Regulatory Guide (RG) 1.121, "Bases for Plugging Degraded PWR Steam Generator Tubes," margins against burst are maintained during normal and postulated accident conditions. The limited inspection length of 8 inches supplies the necessary resistive force to preclude pullout loads under both normal operating and accident conditions. The contact pressure results from the hydraulic expansion process, thermal expansion mismatch between the tube and tubesheet and from the differential pressure between the primary and secondary side. Therefore, the proposed change does not result in a significant increase in the probability or consequence of a SGTR.

The probability of a SLB is unaffected by the potential failure of a SG tube as the failure of a tube is not an initiator for a SLB event. SLB leakage is limited by leakage flow restrictions resulting from the crack and tube-to-tubesheet contact pressures that provide a restricted leakage path above the indications and also limit the degree of crack face opening compared to free span indications. The leak rate during postulated accident conditions would be expected to be less than twice that during normal operation for indications near the bottom of the tubesheet (including indications in the tube end welds) based on the observation that while the driving pressure increases by about a factor of two, the flow resistance increases with an increase in the tube-to-tubesheet contact pressure. While such a decrease is rationally expected, the postulated accident leak rate is bounded by twice the normal operating leak rate if the increase in contact pressure is ignored. Since normal operating leakage is limited to 0.10 gpm (150 gpd), the attendant accident condition leak rate, assuming all leakage to be from indications below 8 inches from the top of the tubesheet would be bounded by 0.187 gpm. This value is bounded by

the 0.35 gpm leak rate assumed in Section 15.4.2, "Major Secondary System Pipe Rupture" of the Salem Unit 1 Updated FSAR.

Based on the above, the performance criteria of NEI-97-06, Rev. 2 and draft RG 1.121 continue to be met and the proposed change does not involve a significant increase in the probability or consequences of an accident previously evaluated.

2. Does the change create the possibility of a new or different kind of accident from any accident previously evaluated?

The proposed change does not introduce any changes or mechanisms that create the possibility of a new or different kind of accident. Tube bundle integrity is expected to be maintained for all plant conditions upon implementation of the limited tubesheet inspection depth methodology. The proposed changes do not introduce any new equipment or any change to existing equipment. No new effects on existing equipment are created nor are any new malfunctions introduced.

Therefore, based on the above evaluation, the proposed changes do not create the possibility of a new or different kind of accident from any accident previously evaluated.

3. Does the change involve a significant reduction in a margin of safety?

The proposed change maintains the required structural margins of the steam generator tubes for both normal and accident conditions. NEI 97-06, Rev. 2 and RG 1.121 are used as the basis in the development of the limited tubesheet inspection depth methodology for determining that steam generator tube integrity considerations are maintained within acceptable limits. RG 1.121 describes a method acceptable to the NRC staff for meeting General Design Criteria 14, 15, 31, and 32 by reducing the probability and consequences of an SGTR. RG 1.121 concludes that by determining the limiting safe conditions of tube wall degradation beyond which tubes with unacceptable cracking, as established by inservice inspection, should be removed from service or repaired, the probability and consequences of a SGTR are reduced. This RG uses safety factors on loads for tube burst that are consistent with the requirements of Section III of the ASME Code.

For axially oriented cracking located within the tubesheet, tube burst is precluded due to the presence of the tubesheet. For circumferentially oriented cracking, Reference 1 defines a length of nondegraded expanded tube in the tubesheet that provides the necessary resistance to tube pullout due to the pressure induced forces (with applicable safety factors applied). Application of the limited tubesheet inspection depth criteria will not result in unacceptable primary-to-secondary leakage during all plant conditions.

Plugging of the steam generator tubes reduces the reactor coolant flow margin for core cooling. Implementation of the 8 inch inspection length at Salem Unit 1 on a permanent basis will result in maintaining the margin of flow that may have otherwise been reduced by tube plugging.

Based on the above, it is concluded that the proposed changes do not result in any reduction of margin with respect to plant safety as defined in the Final Safety Analysis Report Update or bases of the plant Technical Specifications.

7.0 CONCLUSION

Based on the above, PSEG Nuclear LLC concludes that the changes proposed by this License Amendment Request satisfy the no significant hazards consideration standards of 10 CFR 50.92(c), and accordingly a no significant hazards finding is justified.

8.0 PRECEDENTS

The following precedents represent similar amendments recently approved by the NRC:

1) Braidwood Station, Units 1 and 2, - Issuance of Exigent Amendments RE: Revision of Scope of Steam Generator Inspections for Unit 2 Refueling Outage 11 – (TAC Nos. MC6689 and MC6757), dated April 25, 2005.
2) Wolf Creek Generating Station – Issuance of Exigent Amendment RE: Steam Generator (SG) Tube Surveillance Program (TAC No. MC6757), dated April 28, 2005.
3) Byron Station, Unit 2 – Issuance of Amendment (TAC No. MC7219), dated September 19, 2005.
4) Vogtle Electric Generating Plant, Units 1 and 2 RE: Issuance of Amendments Regarding the Steam Generator Tube Surveillance Program (TAC Nos. MC8078 and MC8079), dated September 21, 2005.

TECHNICAL SPECIFICATION PAGES WITH PROPOSED CHANGES

The following Technical Specifications for Salem Unit 1 Facility Operating License DPR-70 are affected by this change request:

Technical Specification	Page
TS 6.8.4.i, "Steam Generator (SG) Program"	6-19c
TS 6.9.1.10, "Steam Generator Tube Inspection Report"	6-24a
TS 6.9.1.5.b, "Reports"	6-21
TS B3/4.4.5, "Steam Generator (SG) Tube Integrity"	B 3/4 4-2

outage during which the SG tubes are inspected or plugged to confirm that the performance criteria are being met.

b. Performance criteria for SG tube integrity. SG tube integrity shall be maintained by meeting the performance criteria for tube structural integrity, accident induced leakage, and operational leakage.

1. Structural integrity performance criterion: All in-service steam generator tubes shall retain structural integrity over the full range of normal operating conditions (including startup, operation in the power range, hot standby, and cool down and all anticipated transients included in the design specification) and design basis accidents. This includes retaining a safety factor of 3.0 against burst under normal steady state full power operation primary-to-secondary pressure differential and a safety factor of 1.4 against burst applied to the design basis accident primary-to-secondary pressure differentials. Apart from the above requirements, additional loading conditions associated with the design basis accidents, or combination of accidents in accordance with the design and licensing basis, shall also be evaluated to determine if the associated loads contribute significantly to burst or collapse. In the assessment of tube integrity, those loads that do significantly affect burst or collapse shall be determined and assessed in combination with the loads due to pressure with a safety factor of 1.2 on the combined primary loads and 1.0 on axial secondary loads.

2. Accident induced leakage performance criterion: The primary-to-secondary accident induced leakage rate for any design basis accident, other than a SG tube rupture, shall not exceed the leakage rate assumed in the accident analysis in terms of total leakage rate for all SGs and leakage rate for an individual SG. Leakage is not to exceed 1 gallon per minute per SG.

3. The operational leakage performance criterion is specified in LCO 3.4.6.2, "Reactor Coolant System Operational Leakage."

c. Provisions for SG tube repair criteria. Tubes found by inservice inspection to contain flaws with a depth equal to or exceeding 40% of the nominal tube wall thickness shall be plugged.

INSERT A HERE

d. Provisions for SG tube inspections. Periodic SG tube inspections shall be performed. The number and portions of the tubes inspected and methods of inspection shall be performed with the objective of detecting flaws of any type (e.g., volumetric flaws, axial and circumferential cracks) that may be present along the length of the ~~tube, from the tube-to-tubesheet weld at the tube inlet to the tube-to-tubesheet weld at the tube outlet, and that may satisfy the applicable tube repair criteria.~~ The tube-to-tubesheet weld is not part of the tube. In addition to meeting the requirements of d.1, d.2, and d.3 below, the inspection scope, inspection methods, and inspection intervals shall be such as to ensure that SG tube integrity is maintained until the next SG inspection. An assessment of degradation shall be performed to determine the type and location of flaws to which the tubes may be susceptible and,

tube beginning 8 inches from the top of the tubesheet on the tube hot leg side to 8 inches below the top of the tubesheet on the tube cold leg side as defined in LCR S06-011 (including WCAP-16640-P).

INSERT A

In lieu of the 40% of the nominal wall thickness repair criteria, the portion of the tube within the tubesheet of the inspected SGs shall be plugged in accordance with the alternate repair criteria defined in LCR S06-011 (including WCAP-16640-P, "Steam Generator Alternate Repair Criteria for the Tube Portion within the Tubesheet at Salem Unit 1.") Degradation found in the portion of the tube below 8 inches from the top of the tubesheet does not require plugging. Degradation identified in the portion of the tube from the top of the tubesheet to 8 inches below the top of the tubesheet shall be plugged on detection.

6.9.1.5 Reports required on an annual basis shall include:

a. DELETED

DELETED

b. ~~The complete results of steam generator tube inservice inspections performed during the report period (reference Specification 4.4.5.5.b).~~

c. The results of any specific activity analyses in which the primary coolant exceeded the limits of Specification 3.4.8. The following information shall be included: (1) Reactor power history starting 48 hours prior to the first sample in which the limit was exceeded; (2) Results of the last isotopic analysis for radioiodine performed prior to exceeding the limit, results of analysis while the limit was exceeded and results of one analysis after the radioiodine activity was reduced to less than the limit. Each result should include date and time of sampling and the radioiodine concentrations; (3) Clean-up system flow history starting 48 hours prior to the first sample in which the limit was exceeded; (4) Graph of the I-131 concentration and one other radioiodine isotope concentration in microcuries per gram as a function of time for the duration of the specific activity above the steady state level; and (5) The time duration when the specific activity of the primary coolant exceeded the radioiodine limit.

MONTHLY OPERATING REPORT

6.9.1.6 DELETED

2. WCAP-8385, Power Distribution Control and Load Following Procedures - Topical Report, September 1974 (W Proprietary) Methodology for Specification 3/4.2.1 Axial Flux Difference. Approved by Safety Evaluation dated January 31, 1978.

3. WCAP-10054-P-A, Rev. 1, Westinghouse Small Break ECCS Evaluation Model Using NOTRUMP Code, August 1985 (W Proprietary), Methodology for Specification 3/4.2.2 Heat Flux Hot Channel Factor. Approved for Salem by NRC letter dated August 25, 1993.

4. WCAP-10266-P-A, Rev. 2, The 1981 Version of Westinghouse Evaluation Model Using BASH Code, Rev. 2. March 1987 (W Proprietary) Methodology for Specification 3/4.2.2 Heat Flux Hot Channel Factor. Approved by Safety Evaluation dated November 13, 1986.

5. WCAP-12472-P-A, BEACON - Core Monitoring and Operations Support System, Revision 0, (W Proprietary). Approved February 1994.

6. CENPD-397-P-A, Rev. 1, Improved Flow Measurement Accuracy Using Crossflow Ultrasonic Flow Measurement Technology, May 2000.

c. The core operating limits shall be determined such that all applicable limits (e.g., fuel thermal mechanical limits, core thermal hydraulic limits, Emergency Core Cooling Systems (ECCS) limits, nuclear limits such as SDM, transient analysis limits, and accident analysis limits) of the safety analysis are met.

d. The COLR, including any mid-cycle revisions or supplements, shall be provided upon issuance for each reload cycle to the NRC.

6.9.1.10 STEAM GENERATOR TUBE INSPECTION REPORT

A report shall be submitted within 180 days after the initial entry into HOT SHUTDOWN following completion of an inspection performed in accordance with the Specification 6.8.4.i, "Steam Generator (SG) Program." The report shall include:

a. The scope of inspections performed on each SG,

b. Active degradation mechanisms found,

c. Nondestructive examination techniques utilized for each degradation mechanism,

INSERT B HERE

d. Location, orientation (if linear), and measured sizes (if available) of service induced indications,

g. ~~e.~~ Number of tubes plugged during the inspection outage for each active degradation mechanism,

h. ~~f.~~ Total number and percentage of tubes plugged to date, and

i. ~~g.~~ The results of condition monitoring, including the results of tube pulls and in-situ testing.

INSERT B

e. The number of indications detected in the upper 8 inches of the tubesheet thickness along with their location, measured size, orientation, and whether the indication initiated on the primary or secondary side.

f. The operational primary to secondary leakage rate observed in each steam generator during the cycle preceding the inspection and the calculated accident leakage rate for each steam generator from the lowermost 13 inches of tubing (the tubesheet is nominally 21.03 inches thick) for the most limiting accident. If the calculated leak rate is less than 2 times the total observed operational leakage rate, the 180 day report should describe how the calculated leak rate is determined.

beginning 8 inches from the top of the tubesheet on the tube hot leg side to 8 inches below the top of the tubesheet on the tube cold leg side as defined in LCR S06-011 (including WCAP-16640-P).

3/4.4.4 PRESSURIZER

The limit on the maximum water volume in the pressurizer assures that the parameter is maintained within the normal steady-state envelope of operation assumed in the SAR. The limit is consistent with the initial SAR assumptions. The 12 hour periodic surveillance is sufficient to assure that the parameter is restored to within its limit following expected transient operation. The maximum water volume also ensures that a steam bubble is formed and thus the RCS is not a hydraulically solid system. The requirement that a minimum number of pressurizer heaters be OPERABLE assures that the plant will be able to establish natural circulation.

3/4.4.5 STEAM GENERATOR (SG) TUBE INTEGRITY

The LCO requires that SG tube integrity be maintained. The LCO also requires that all SG tubes that satisfy the repair criteria be plugged in accordance with the Steam Generator Program.

During an SG inspection, any inspected tube that satisfies the Steam Generator Program repair criteria is removed from service by plugging. If a tube was determined to satisfy the repair criteria but was not plugged, the tube may still have tube integrity.

In the context of this Specification, a SG tube is defined as the ~~entire~~ length of the tube~~, including the tube wall, between the tube-to-tubesheet weld at the tube inlet and the tube-to-tubesheet weld at the tube outlet.~~ The tube-to-tubesheet weld is not considered part of the tube.

A SG tube has tube integrity when it satisfies the SG performance criteria. The SG performance criteria are defined in Specification 6.8.4.i, "Steam Generator (SG) Program," and describe acceptable SG tube performance. The Steam Generator Program also provides the evaluation process for determining conformance with the SG performance criteria.

There are three SG performance criteria: structural integrity, accident induced leakage, and operational leakage. Failure to meet any one of these criteria is considered failure to meet the LCO.

The structural integrity performance criterion provides a margin of safety against tube burst or collapse under normal and accident conditions, and ensures structural integrity of the SG tubes under all anticipated transients included in the design specification. Tube burst is defined as, "The gross structural failure of the tube wall. The condition typically corresponds to an unstable opening displacement (e.g., opening area increased in response to constant pressure) accompanied by ductile (plastic) tearing of the tube material at the ends of the degradation." Tube collapse is defined as, "For the load displacement curve for a given structure, collapse occurs at the top of the load versus displacement curve where the slope of the curve becomes zero." The structural integrity performance criterion provides guidance on assessing loads that significantly affect burst or collapse. In that context, the term "significant" is defined as, "An accident loading condition other than differential pressure is considered significant when the addition of such loads in the assessment of the structural integrity performance criterion could cause a lower structural limit or limiting burst/collapse condition to be established." The determination of whether thermal loads are primary or secondary loads is based on the ASME definition