ENCLOSURE 5

WCAP-16506-NP

Westinghouse Non-Proprietary Class 3

WCAP-16506-NP

December 2005

Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet at Turkey Point Units 3 and 4

Westinghouse

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Steam Generator Alternate Repair Criteria for Tube Portion Within the Tubesheet at Turkey Point Units 3 and 4

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December 2005

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This work was performed under WOG Project Number PA-MSC-0199

***Official Record Electronically Approved in EDMS.**

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Westinghouse Owners Group
Member Participation* for WOG Project PA-MSC-0199

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	Surry 1 & 2 (W)	X	
Dominion VA	North Anna Unit 1 and 2 (W)		X
Duke Energy	Catawba 1 & 2, McGuire 1 & 2 (W)		X
Entergy Nuclear Northeast	Indian Point 2 & 3 (W)		X
Entergy Operations South	Arkansas 2, Waterford 3 (CE)		X
Exelon Generation Co. LLC	Braidwood 1 & 2, Byron 1 & 2 (W)		X
FirstEnergy Nuclear Operating Co	Beaver Valley 1 & 2 (W)		X
Florida Power & Light Group	St. Lucie 1 & 2 (CE)		X
Florida Power & Light Group	Turkey Point 3 & 4	X	
Florida Power & Light Group	Seabrook (W)		X
Nuclear Management Company	Prairie Island 1 & 2, Point Beach 1 & 2 (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)		X
PSEG – Nuclear	Salem 1 & 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 & 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 WOG Program Management Office to verify participation before sending this document to participants not listed above.

Westinghouse Owners Group
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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 1, 2, 3 & 4 Yonggwang 1 & 2 (W)		X
Korea Hydro & Nuclear Power Corp.	Yonggwang 3, 4, 5 & 6 Ulchin 3, 4 & 5 (CE)		X
Nuklearna Electrarna KRSKO	Krško (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 WOG Program Management Office to verify participation before sending documents to participants not listed above.

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1.0 EXECUTIVE SUMMARY

Nondestructive examination indications of primary water stress corrosion cracking were found in the Alloy 600 thermally treated Westinghouse Model D5 steam generator (SG) 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 approximately 3/4 inch above the bottom of the tube, 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. Based on interpretations of requirements published by the NRC staff in GL 2004-01, Florida Power and Light Company requested that a recommendation be developed for future examinations of the Westinghouse Model 44F steam generator tubesheet regions at the Turkey Point Units 3 and 4 power plants. 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 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 4.78 to 8.04 inches, designated as H*, and, 2) that the accident condition leak rate integrity can be conservatively bounded by twice the normal operation leak rate from degradation of any magnitude below 17 inches from the top of the nominally 21.81 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 leak rate resistance increases below 17 inches from the top of the tubesheet. Internal tube bulges within the tubesheet are created in a number of tubes as an artifact of the manufacturing process. The possibility of additional degradation at such locations exists based on the already reported degradation at Catawba 2 and Vogtle 1. 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. Application of the structural analysis and bounding leak rate evaluation results to eliminate inspection and/or repair of indications below the 17 inch elevation from the top of the tubesheet 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.

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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. It is believed that the rolling process was used during fabrication of the Turkey Point Units 3 and 4 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 4 to 6 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 Turkey Point Units 3 and 4 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 Turkey Point Units 3 and 4 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.

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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. Another plant with Westinghouse Model D5 steam generators, which belongs to another utility, has inspected 3% of the tubes in the hot leg of all steam generators from 3 inches above to 21 inches below the top of the tubesheet during 2R08 and has reported no indications. 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 each of two 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 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. Nor during similar inspections at Byron 2 (Model D5 SGs) and Vogtle 1 (Model F SGs) in the fall of 2005.

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 Turkey Point Units 3 and 4 were completed by a mechanical roll process as they were shipped in 1979.

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 17 inches below the top of the tubesheet below which degradation of any extent would not adversely affect SG performance criteria .

A summary of the analysis results is provided in Section 4.0 of this report. Section 5.0 addresses plant operating conditions at Turkey Point Units 3 and 4. Section 6.0 discusses the tube pullout and leakage test programs that are applicable to the Model 44F SGs at Turkey Point Units 3 and 4. 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 review of the qualitative arguments used by the NRC Staff for the tube joint inspection length approved for other plants is discussed in Section 9.0. Overall conclusions are contained in Section 10.0 of this report.

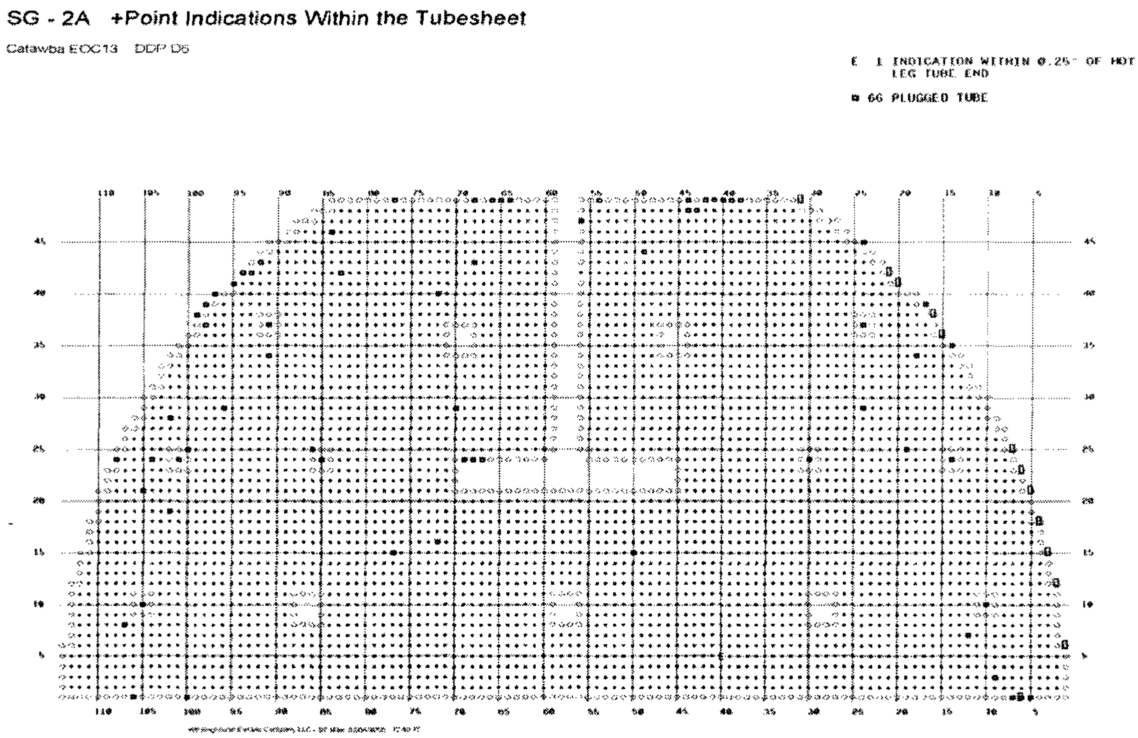

Figure 3-1. Distribution of Indications in SG A at Catawba 2

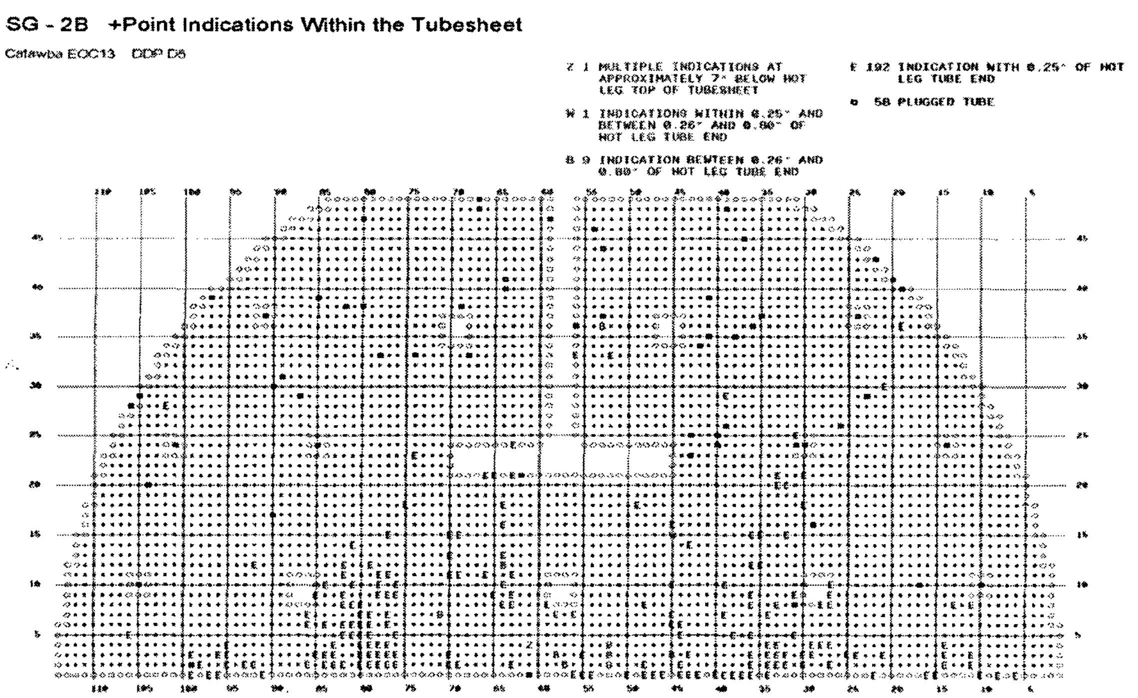

Figure 3-2. Distribution of Indications in SG B at Catawba 2

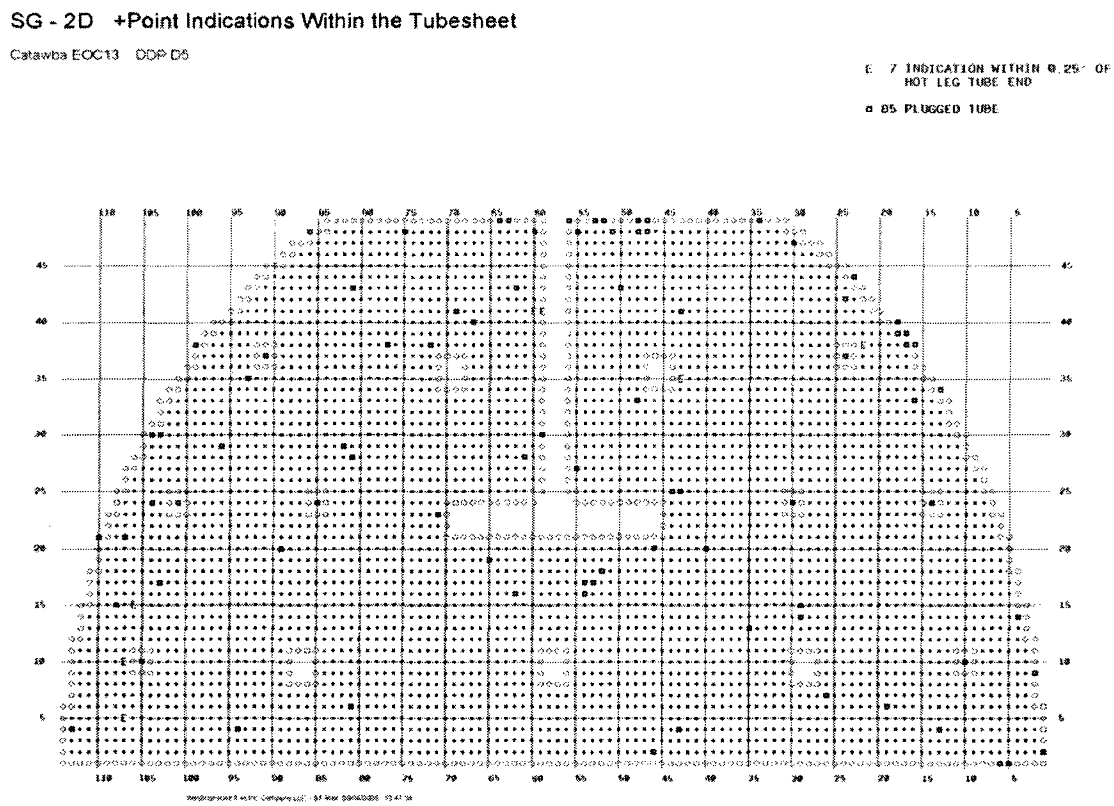

Figure 3-3. Distribution of Indications in SG D at Catawba 2

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 4.78 to 8.04 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 17 inches from the top of the 21.81 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 that adversely impact structural integrity are transmitted below the H* elevation, and the contact leak rate resistance increases below the 17 inch 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 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 support a license amendment to eliminate inspection of the region of the tube below 17 inches from the top of the tubesheet. 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. Potential degradation regions excluded from examination would be limited to below 17 inches of the tube in a nominally 21.81 inch thick tubesheet, which is well below the mid-plane of the tubesheet.

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, for example see Reference 34. Subsequent approvals were also obtained for use at Byron 2 and Vogtle 2 in their fall 2005 inspection campaigns, Reference 13 for example. This report was prepared to justify the inspection exclusion zone to the portion of the tube below 17 inches from the top of the tubesheet and to provide the necessary information for a detailed NRC staff review of the technical basis for that request.

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, Reference 6. 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. The structural model is 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 as hardrolled 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 H* model for leak rate is such a model and it has not been previously reviewed by the NRC staff. An alternative approach was developed for application at Turkey Point Units 3 and 4 from engineering expectations of the relative leak rate between normal operation and postulated accident conditions based on a first principles engineering approach and is included herein.

5.0 OPERATING CONDITIONS

Turkey Point Units 3 and 4 are three-loop nuclear power plants with Westinghouse designed and fabricated Model 44F SGs; there are 3214 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 Turkey Point Units 3 and 4 SG thermal and hydraulic parameters during normal operation are tabulated below (Note: these values assume a 20% SG tube plugging level.):

Parameter and Units		Bounding Operating Conditions (1)
Power – NSSS	MWt	2308
Reactor Vessel Outlet Temperature	°F	597.2
Reactor Coolant System Pressure	psig	2235
SG Steam Temperature	°F	503.2
SG Steam Pressure	psig	735
Steam Line Break Pressure	psig	2560
(1) Reference 24		

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 well within the guideline values of 10 CFR 100.

6.0 STEAM GENERATOR TUBE LEAKAGE AND PULLOUT TEST PROGRAMS 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 14 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 15 was originally written for the Wolf Creek SGs. The testing also provides valuable information regarding the calculation of the 17 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, Reference 16. The data from both sets of testing programs were combined to support the development of the inspection criteria delineated in this report for Model 44F 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 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 and Model F SGs. The information is provided as a consistency check for the Model 44F steam generator tube pullout results and to provide a discussion of the basis for the SLB leakage results.

- 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 17 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 18, directly supports the determination of both the H* and the 17 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 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 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

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 some previous similar 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 (expansions for the Turkey Point Units 3 and 4 SGs were in a range of 31 to 34 ksi), the samples were subject to a high temperature soak to simulate the stress relief of the channel head-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 21 for data from several samples.

In the 600°F pullout tests, the lowest value for small displacement load 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. The results are analyzed in Section 7.0 of this report.

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 Section 6.2.1, followed by the description and results of the Model D5 program in Section 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

[ ][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:

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 exceed 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 and D5 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,

$$\ln(K) = b_0 + b_1 P_c \qquad (1)$$

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 combined Model F and Model D5 loss coefficient data follow a relation of the form,

$$K = e^{b_0 + b_1 P_c}, \qquad (2)$$

where the Model D5 data were adjusted to correspond to the diameter of an installed Model F tube. The absolute leak rates *per se* are not used in the determination the 17 inch inspection length and the confidence curve on the charts is provided for information only. Since the 17 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. No additional leakage testing was conducted for the Model 44F SGs (See Section 9.2.1 of this report for further discussion).

Table 6-1. Model F Leak Test Program Matrix

a,c,e

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

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 & D5 Leak Rate Analysis

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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 Turkey Point Units 3 and 4 replacement SGs was the 1965 edition with the Summer 1965 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 Turkey Point Unit 3 and 4 Model 44F SGs and the results are reported below. Typical Model 44F 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 4.78 to 8.04 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 strength of the weld from axial cracks, and tubes with circumferential cracks up to about 180° by 100% deep would have sufficient strength to meet the nominal ASME Code structural requirements, based on the margins of safety reported in Reference 22.

An examination of Table 7-7 through Table 7-9 illustrates that the holding power of the tube-to-tubesheet joint at a depth of 17 inches from the top of the tubesheet is much greater than at the top of the tubesheet in the range of H* (Reference 23). 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 23.2 inches, however, in order to establish a H* value that was conservative throughout the zone, the tube location for which the analysis results were reported, is at a radius of 3.73 inches. This value is conservative above the neutral surface of the tubesheet for all tubes in

[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.

Zone C. Likewise for tubes in Zone B under the heading 34.4 inches, the basis for the calculation was a tube at a radius of 23.2 inches. The purpose of this discussion is to illustrate the extreme conservatism associated with the holding power of the joint below the neutral surface of the tubesheet, and to identify the proper tube radii for consideration. In the center of the tubesheet, the incremental holding strength in the 2.37 inch range from 14.00 to 16.37 inches below the top of the tubesheet is 1519 lbf per inch during normal operation, which meets the performance criterion of 3.0 ΔP with the first 1.85 inch of engagement above 17 inches. At the radius, the performance criterion for 1.4·SLB ΔP is met by the first 1.2 inch of engagement above 17 inches. At a radius of 45.5 inches the corresponding length of engagement needed is about 1.3 inches. In other words, while a value of 8.04 inches was determined for H* from the top of the tubesheet, a length of 1.2 to 1.3 inches would be sufficient at the bottom of the inspection length.

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

A finite element model was developed for the Model 44F tubesheet, channel head, and shell region to determine the tubesheet hole dilations in the Turkey Point Units 3 and 4 steam generators. [

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

7.1.1 Material Properties and Tubesheet Equivalent Properties

The tubes in the Turkey Point Units 3 and 4 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 23, and match the properties listed in the ASME Code.

The perforated tubesheet in the Model 44F 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 25), 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 1.2344 inches and nominal hole diameters are 0.893 inch. The ID of the tube after expansion into the tubesheet is taken to be 0.794 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: $h_{nominal}$ = $P_{nominal} - d_{maximum}$
$P_{nominal}$ = 1.2344 inches, the pitch of the square hole pattern
$d_{maximum}$ = .893 inches, the tube hole diameter

Therefore, $h_{nominal}$ = 0.3414 inches (1.2344-0.893), and η = 0.2766 when the tubes are not included. From Slot, Reference 29, the in-plane mechanical properties for Poisson's ratio of 0.3 are:

Property	Value
E*p/E	0.3945
v*p	0.1615
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 26) elements used for the global model is σ_{xx}, σ_{yy}, τ_{xy}, and σ_{zz}. The mapping between the Reference 25 equations and WECAN/+ is therefore:

Coordinate Mapping	
Reference 25	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 44F SG channel head/tubesheet/shell region (which includes the Turkey Point Units 3 and 4 steam generator) 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 26). The model for the Model 44F 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:

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

#Negative sign denotes direction of load in WECAN/PLUS

where, R_i = Inside radius of secondary shell in finite element model = 60.845in.
R_o = Outside radius of secondary shell in finite element model = 64.655 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 27), 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 = 0.397 in.
c = Outside radius of tube = 0.4465 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 (2.25 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 28 and 16).

The tube inside and outside radii within the tubesheet are obtained by assuming a nominal diameter for the hole in the tubesheet (0.893 inch) and wall thinning in the tube equal to the average of that measured during hydraulic expansion tests. The final wall thickness is 0.0495 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.	0.397
c, outside tube radius, in.	0.4465
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	$7.82 \cdot 10^{-6}$
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 Turkey Point 3 and 4 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 References 25 and 30. The current operating parameters from Reference 24 are used. The temperatures and pressures for normal operating conditions at Turkey Point Units 3 and 4 are bracketed by the following case:

Loading	$T_{max}^{(2)}$
Primary Pressure	2235 psig
Secondary Pressure	735 psig
Primary Fluid Temperature (T_{hot})	597.2°F
Secondary Fluid Temperature	510.7°F

The primary pressure [

]^{a,c,e}.

7.1.4.2 Faulted Conditions

Of the faulted conditions, Steamline Break (SLB) is the most limiting.

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 27, 16 and 18). 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 212°F at approximately 2000 seconds for three loop plants. 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})	=	212°F
Secondary Fluid Temperature	=	212°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 Turkey Point Units 3 and 4, the contact pressures between the tube and tubesheet for various plant conditions are listed in Table 7-6 and plotted versus radius on Figure 7-3 through Figure 7-5. 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 [

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The end cap loads for Normal and Faulted conditions are:

Normal (maximum):	$\pi \cdot (2235\text{-}735) \cdot (0.893)^2 /4 = 939$ lbs.
Faulted (SLB):	$\pi \cdot 2560 \cdot (0.893)^2 /4 = 1603$ 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*.

A series of tests were performed to determine the residual strength of the joint. The data from this series of pullout tests are listed in Reference 21 and in Table 7-10. Three (3) each of the tests were performed at room temperature, 400°F, and 600°F. [

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

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

where: F_{HE} = Resistance to pull out due to the initial hydraulic expansion = 614.18 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 Table 7-7 through Table 7-9, 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 Table 7-7 through Table 7-9. The above equation is also used to find the minimum contact lengths needed to meet the pullout force requirements. In Zone C (See Figure 7-1), the length calculated was 7.07 inches for the 3 times the normal operating pressure performance criterion which corresponds to a pullout force of 2818 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 C, this length is 8.04 inches for the 1.4 times the accident pressure performance criterion which corresponds to a pullout force of 2245 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 23. The H* results for each zone are summarized in Table 7-12.

Therefore, the bounding condition for the determination of the H* length is the SLB performance criterion for Zones A, B and C. The minimum contact length for the SLB faulted condition is 8.04 inches in Zone C. In Zone B, the minimum contact length is 6.82 inches. In Zone A, the minimum contact length is calculated to be 4.48 inches.

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·10^6)	31.00	30.20	29.90	29.50	29.00	28.70	28.20
Thermal Expansion (in/in/°F·10^{-6})	6.90	7.20	7.40	7.57	7.70	7.82	7.94
Density (lb-sec^2/in^4·10^{-4})	7.94	7.92	7.90	7.89	7.87	7.85	7.83
Thermal Conductivity (Btu/sec-in-°F·10^{-4})	2.01	2.11	2.22	2.34	2.45	2.57	2.68
Specific Heat (Btu-in/lb-sec^2-°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·10^6)	29.20	28.50	28.00	27.40	27.00	26.40	25.30
Thermal Expansion (in/in/°F·10^{-6})	6.50	6.67	6.87	7.07	7.25	7.42	7.59
Density (lb-sec^2/in^4·10^{-4})	7.32	7.30	7.29	7.27	7.26	7.24	7.22
Thermal Conductivity (Btu/sec-in-°F·10^{-4})	5.49	5.56	5.53	5.46	5.35	5.19	5.02
Specific Heat (Btu-in/lb-sec^2-°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·10^6)	29.20	28.50	28.00	27.40	27.00	26.40	25.30
Thermal Expansion (in/in/°F·10^{-6})	7.06	7.25	7.43	7.58	7.70	7.83	7.94
Density (lb-sec^2/in^4·10^{-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 & H*
for Turkey Point Units 3 & 4 Steam Generators

a,c,e

+ Negative contact pressure indicates a gap condition.

Table 7-7. Cumulative Forces Resisting Pull Out from the TTS Turkey Point Units 3 and 4
Hot Leg Normal Conditions, Tavg Coastdown, 20% SGTP

a,c,e

+Negative contact pressure indicates a gap condition.

Table 7-8. Cumulative Forces Resisting Pull Out from the TTS Turkey Point Units 3 and 4 Cold Leg Normal Conditions Tave Coastdown with 20% Plugging

a,c,e

+ Negative contact pressure indicates a gap condition.

Table 7-9. Cumulative Forces Resisting Pull Out from the TTS Turkey Point Units 3 and 4 Faulted (SLB) Conditions

a,c,e

+ Negative contact pressure indicates a gap condition

Table 7-10. 0.25 Inch Displacement Pullout Test Data

a,c,e

Table 7-11. Summary of H* Calculations for Turkey Point Units 3 and 4

a,c,e

Table 7-12. H* Summary Table		
Zone	Limiting Loading Condition	Engagement from TTS (inches)
A	1.4 SLB ΔP	4.78 [2]
B	1.4 SLB ΔP	6.82
C	1.4 SLB ΔP	8.04

Notes:
1. Seismic loads have been considered and are not significant in the tube joint region (Reference 22).
2. 0.3 inches added to the maximum calculated H* for Zone A to account for the hydraulic expansion transition region at the top of the tubesheet.

a,c,e

Figure 7-1. Definition of H* Zones (Reference 35)

a,c,e

Figure 7-2. Finite Element Model of Model 44F Tubesheet Region

a,c,e

Figure 7-3. Contact Pressures for Normal Condition (Hot Leg) at Turkey Point Units 3 & 4

a,c,e

Figure 7-4. Contact Pressures for Normal Condition (Cold Leg) at Turkey Point Units 3 & 4

a,c,e

Figure 7-5. Contact Pressures for SLB Faulted Condition at Turkey Point Units 3 & 4

a,c,e

Figure 7-6. Model 44F Pullout Test Results for Force/inch at 0.25 inch Displacement

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8.0 LEAK RATE ANALYSIS OF CRACKED TUBE-TO-TUBESHEET 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 below 17 inches (i.e., the elevation of the neutral plane) from the top of the tubesheet. It is noted that the methods discussed below support a permanent change to the Turkey Point Units 3 and 4 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 31 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

[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.

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 23.2 inches from the center of the SG, the contact pressure during normal operation is calculated to be 2645 to 2563 psi[2], see the last contact pressure entry in the center columns of Table 7-8 and Table 7-7, respectively, while the contact pressure during a postulated steam line break would be on the order of 4054 psi at the bottom of the tubesheet at a radius of 23.227 inches, see Table 7-9. 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 32 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.,

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

[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.

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 7-9 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 8-3 is more on the order of 9% 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 16.4 inches from the top of the tubesheet the contact pressure increases by about 39% at a radius of 3.73 inches relative to that during normal operation. The flow resistance would be expected to increase by about 38%, 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 16.4, 10.9 and 5.4 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 1409 psi near the center of the tubesheet and exhibits no change at a radius of about 60.0 inches.

At 16.4 inches below the top of the tubesheet (a little over 5.4 inches from the bottom) the tubesheet the contact pressure increases by about 739 psi at the center to a minimum of approximately 100 psid at a radius of 50 inches, Figure 8-2. The contact pressure during a SLB is everywhere greater than that during NOp. The influence of the channel head and shell at the periphery causes the deformation to become non-uniform near the periphery.

At roughly the neutral surface, about 10.9 inches, Figure 8-3, the contact pressure during SLB is uniformly greater than that during normal operation by approximately 110 psi (ranging from 110 to 200 psid traversing outward).

At a depth of 5.44 inches from the TTS, Figure 8-4, the contact pressure decreases by about 529 psid near the center of the TS to a decrease of 276 psid at a radius of 45.52 inches.

A comparison of the curves at the various elevations leads to the conclusion that for a length of 5.5 inches upward from an elevation of 5.4 inches above the bottom of the tubesheet (i.e., at the neutral plane) there is always an increase in the contact pressure in going from normal operation conditions to postulated SLB conditions. Hence, it is reasonable to omit any consideration of inspection of bulges or other artifacts below a depth of 10.9 inches from the top of the tubesheet. Therefore, applying a very conservative inspection sampling length of 17 inches downward from the top of the tubesheet during the Turkey Point Units 3 and 4 outages provides a high level of confidence that the potential leak rate from indications below the lower bound inspection elevation during a postulated SLB event will be bounded by twice the normal operation primary-to-secondary leak rate.

Noting that the density of the number of tubes populating the tubesheet increases with the square of the radius, the number of tubes for which the contact pressure is greater during a SLB than during NOp at a depth of 11 inches from the TTS is far greater than the number for which the contact pressure decreases.

8.2 LIGAMENT TEARING DISCUSSION

One of the concerns that must be addressed in dealing with cracks in SG tubes is the potential for ligament tearing to occur during a postulated accident when the differential pressure is significantly greater than during normal operation. While this is accounted for in the strength evaluations that demonstrate a resistance to pullout in excess of $3 \cdot \Delta P$ for normal operation and $1.4 \cdot \Delta P$ for postulated accident conditions, the potential for ligament tearing to significantly affect the leak rate predictions needs to be accounted for.

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 [

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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 43 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 (Reference 33).

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

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Figure 8-2. Change in Contact Pressure at 16.4 Inches Below the TTS

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Figure 8-3. Change in Contact Pressure at 10.90 Inches Below the TTS

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Figure 8-4. Change in Contact Pressure at 5.44 Inches Below the TTS

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9.0 NRC STAFF DISCUSSION FOR ONE CYCLE APPROVAL B* BRAIDWOOD UNIT 2

9.1 JOINT STRUCTURAL INTEGRITY DISCUSSION

As noted in Section 4.1, "Joint Structural Integrity" of Reference 34 and Section 3.1 of Reference 13, the NRC staff stated that the Westinghouse analyses that concluded that the required engagement distances that varies from 3 to 8.6 inches were not reviewed in detail and more qualitative arguments were used by the NRC staff for one time approval of the 17 inch tube joint inspection length. The qualitative arguments are stated below.

- Pullout tests demonstrate that the radial contact pressure produced by the hydraulic expansion alone is such as to require an engagement distance of less than 3 inches to ensure adequate safety margins against pullout. This estimate is a mean minus one standard deviation based on nine pullout tests. The estimate ignores that effect on needed engagement distance from differential thermal expansion, and tubesheet bore dilations associated with tubesheet bow.

- Radial differential thermal expansion between the tube and the tubesheet under hot operating conditions will act to further tighten the joint (i.e., increase radial contact pressure) and to reduce the necessary engagement distance relative to room temperature conditions. The radial differential thermal expansion arises from the fact that Alloy 600 tubing has a slightly higher (by 6 percent) coefficient of thermal expansion than does the SA-508 Class 2a tubesheet material and that tubes are a little hotter than the tubesheet.

- The internal primary pressure inside the tube under normal operating and accident conditions also acts to tighten the joint relative to unpressurized conditions, thus reducing the necessary engagement distance.

- Tubesheet bore dilations cased by the tubesheet bow under primary to secondary pressure can increase or decrease contact pressure depending on tube location within the bundle and the location along the length of the tube in the tubesheet region. Basically, the tubesheet acts as a flat, circular plate under an upward acting net pressure load. The tubesheet is supported axially around its periphery with a partial restraint against tubesheet rotation provided by the steam generator shell and the channel head. The SG divider plate provides a spring support against upward displacement along a diametral mid-line. Over most of the tubesheet away from the periphery, the bending moment from the resulting from the applied primary to secondary pressure load can be expected to put the tubesheet in tension at the top and compression at the bottom. Thus, the resulting distortion of the tubesheet bore (tubesheet bore dilation) tends to be such as to loosen the tube to tubesheet joint at the top of the tubesheet and to tighten the joint at the bottom of the tubesheet. The amount of dilation and resulting change in joint contact pressure would be expected to vary in a linear fashion from the top to the bottom of a tubesheet. Given the neutral axial to be at approximately the mid-point of the tubesheet thickness (i.e., 10.5 inches below the TTS to 17 inches below the TTS), tubesheet bore dilation effects would be expected to further tighten the joint from 10 inches below the TTS to 17 inches below the TTS which would be the lower limit of the proposed tubesheet region inspection zone. Combined with the effects of the tube joint tightening associated with the radial differential thermal expansion and primary pressure inside the tube, contact pressure over at least a 6.5 inch distance should be considerably higher than the contact pressure simulated in the above mentioned pullout tests. A similar logic applied to the periphery of the tubesheet leads the staff to conclude that at the top 10.5 inches of the tubesheet region, contact pressure over at least a 6.5 inch distance should be considerably higher than the contact pressure simulated in the above mentioned pull out tests. Thus, the staff concludes that the proposed 17-inch engagement distance (or inspection zone) is acceptable to ensure the structural integrity of the tubesheet joint.

The NRC qualitative arguments are further supported on a more quantitative basis based on a study completed for the Model F steam generators for another plant (Reference 4). Moreover, similar statements were made in Reference 13 in approving a similar amendment for a plant with Model F SGs.

9.1.1 Discussion of Interference Loads

There are four source terms that must be considered relative to the determination of the interface pressure between the tube and the tubesheet. These are,

1. the initial preload from the installation of the tube,
2. internal pressure in the tube that is transmitted from the ID to the OD,
3. thermal expansion of the tube relative to the tubesheet, and
4. bowing of the tubesheet that results in dilation of the tubesheet holes.

The initial preload results from the plastic deformation of the tube material relative to that of the tubesheet. The material on the inside diameter experiences more plastic deformation than the material on the outside and thus has a deformed diameter which is incrementally greater. Equilibrium of the hoop forces and moments in the tube means that the OD is maintained in a state of hoop tension at a diameter greater than a stress free state. The model for the determination of the initial contact pressure between the tube and the tubesheet, P_c, is illustrated on Figure 9-2. Both the tube and the tubesheet behave as elastic springs after the expansion process is applied. The normal stress on the tube must be equal in magnitude to the normal stress on the tubesheet and the sum of the elastic springback values experienced by each must sum to the total interference.

As long as the tube and the tubesheet remain in contact the radial normal stresses must be in equilibrium. Thus, the problem of solving for the location of the interface and the contact pressure is determinate. The elements considered in the analysis are illustrated on Figure 9-3 for all operating and postulated accident conditions; the centerline of the tube and tubesheet hole are to the left in the figure. Each source of deformation of the tube outside surface starting from the installed equilibrium condition can be visualized starting from the top left side of the figure. The sources of deformation of the tubesheet inside surface can be visualized starting from the lower left side of the figure. As illustrated, although not to scale, the tube material has a coefficient of thermal expansion that is greater than that of the tubesheet. The radial flexibility[1], f, of the tube relative to that of the tubesheet determines how much of the pressure is actually transmitted to the interface between the tube and the tubesheet. Positive radial deformation of the tube in response to an internal pressure is found as the product of the pressure, P_p, and the tube flexibility

[1] Flexibility is the ratio of deformation to load and is the inverse of the stiffness.

associated with an internal pressure, discussed in the next section. Thus, the tube gets tighter in the tubesheet hole as the temperature of the tube and tubesheet increase. The deformation of the tube in response to an external pressure, P_s, is the product of the pressure times the flexibility associated with an external pressure. The normal operation contact pressure, P_N, is found from compatibility and equilibrium considerations. The deformation of the tubesheet hole in response to an internal pressure, P_s, is found as the product of the pressure and the flexibility of the tubesheet associated with an internal pressure. The opening or closing of the tubesheet hole, δr_i, resulting from bow induced by the primary-to-secondary pressure difference is in addition to the deformations associated with temperature and internal pressure. Once the tube has been installed, the deformations of the tube and tubesheet associated thermal expansion, internal pressure, and tubesheet bow remain linearly elastic.

Because of the potential for a crack to be present and the potential for the joint to be leaking, the pressure in the crevice is assumed to vary linearly from the primary pressure at the crack elevation to the secondary pressure at the top of the tubesheet. If the joint is not leaking, it would be expected that there was no significant fluid pressure in the crevice. The pressure assumption is considered to be conservative because it ignores the pressure drop through the crack, and the leak path is through the crevice will not normally be around the entire circumference of the tube. In addition, the leak path is believed to be between contacting microscopic asperities between the tube and the tubesheet, thus the pressure in the crevice would not be acting over the entire surface area of the tube and tubesheet. In any event, pressure in the crevice is always assumed to be present for the analysis.

There is no bow induced increase in the diameter of the holes during normal operation or postulated accident conditions below the mid span elevation within the tubesheet, hence most analyses concentrate on locations near the top of the tubesheet. The tubesheet bow deformation under postulated accident conditions will increase because of the larger pressure difference between the bottom and top of the tubesheet. The components remain elastic and the compatibility and equilibrium equations from the theory of elasticity remain applicable. Below the mid span elevation within the tubesheet the tubesheet holes will contract. The edges of the tubesheet are not totally free to rotate and there is some suppression of the contraction near the outside radius. This also means that the dilation at the top of the tubesheet is also suppressed near the outside radius of the tubesheet. The maximum hole dilations occur near the center of the tubesheet.

The application of the theory of elasticity means that the individual elements of the analysis can be treated as interchangeable if appropriate considerations are made. The thermal expansion of the tube can be thought of as the result of some equivalent internal pressure by ignoring Poisson effects, or that tubesheet bow could be analytically treated as an increase in temperature of the tubesheet while ignoring associated changes in material properties.

9.1.2 Flexibility Discussion

Recall flexibility, f, is defined as the ratio of deflection relative to applied force. It is the inverse of stiffness which commonly used to relate force to deformation. There are four flexibility terms associated with the radial deformation of a cylindrical member depending on the surface to which the loading is applied and the surface for which the deformation is being calculated, e.g., for transmitted internal pressure one is interested in the radial deformation of the OD of the tube and the ID of the tubesheet. The deformation of the OD of the tube in response to external pressure is also of interest. The geometry of the tube-to-tubesheet interface is illustrated on Figure 9-1. The flexibility of the tubesheet, designated herein by the subscript c, in response to an internal pressure, P_{ci}, is found as,

a,c,e

[ ] *Tubesheet* (1)

where, r_{ci} = inside radius of the tubesheet and outside radius of the tube,
r_{co} = outside radius of the tubesheet hole unit cell,
E_c = the elastic modulus of the carbon steel tubesheet material, and
υ = Poisson's ratio for the tubesheet material.

Here, the subscripts on the flexibility stand for the component, c for tubesheet (and later t of tube), the surface being considered, i for inside or o for outside, and the surface being loaded, again, i for inside and o for outside. The superscript designates whether the cylinder is open, o, or closed, c, of interest in dealing with the end cap load from pressure in the tube. The former case is a state of plane stress and the latter is not since a closed cylinder has an end cap load. The flexibility of the tube in response to the application of an external pressure, P_{ro}, e.g., the contact pressure within the tubesheet, is,

a,c,e

[ ] *Open Tube* (2)

Poisson's ratio is the same for the tube and the tubesheet. When the external pressure can act on the end of the tube,

a,c,e

[ ] *Closed Tube* (3)

where E_t is the elastic modulus of the tube material. The flexibility of the tube in response to an external pressure is different when the secondary side pressure is present because that pressure also acts to compress the tube in the axial direction giving rise to a Poisson expansion effect, resisting the radial compression due to the pressure.

Finally, the flexibility of the outside radius of the tube in response to an internal pressure, Pti, is,

a,c,e

[ ] *Closed Tube* (4)

where r_{ti} is the internal radius of the tube and the tube is assumed to be closed. For an open tube the term in parentheses in the numerator is simply 2. A closed tube expands less due to Poisson contraction associated with the end cap load from the internal pressure. A summary of the applicable flexibilities is provided in Table 9-1. Note that during normal operation there is an end cap load on the tube from the secondary pressure but not from that associated with the fluid in the crevice if the joint is leaking. Both flexibilities would then be involved in calculating the radial deformation of the outside of the tube. Only the open tube flexibility is used with the pressure in the crevice for postulated accident conditions.

When the inside of the tube is pressurized, P_{ti}, some of the pressure is absorbed by the deformation of the tube within the tubesheet and some of the pressure is transmitted to the OD of the tube, P_{to}, as a contact pressure with the ID of the tubesheet. The magnitude of the transmitted pressure is found by considering the relative flexibilities of the tube and the tubesheet as,

a,c,e

[ ] (5)

Note that the tube flexibility in response to the contact pressure is for an open tube because there is no end cap load associated with the contact pressure. The denominator of the fraction is also referred to as the interaction coefficient between the tube and the tubesheet. About 85 to 90% of the pressure internal to the tube is transmitted through the tube in Westinghouse designed SGs. However, the contact pressure is not increased by that amount because the TS acts as a spring and the interface moves radially outward in response to the increase in pressure. The net increase in contact pressure is on the order of 56.4% of the increase in the internal pressure. For example, the contact pressure between the tube and the tubesheet is increased by about 1970 psi during normal operation relative to ambient conditions. Likewise, the increase in contact pressure associated with SLB conditions is about 2250 psi relative to ambient conditions.

When the temperature increases from ambient conditions to operating conditions the differential thermal expansion of the tube relative to the tubesheet increases the contact pressure between the tube and the tubesheet. The mismatch in expansion between the tube and the tubesheet, δ, is given by,

$$\delta = \left(\alpha_t \,\Delta T_t - \alpha_c \,\Delta T_c\right) r_{to} \qquad \text{Thermal Mismatch} \tag{6}$$

where: α_t, α_c = thermal expansion coefficient for the tube and tubesheet respectively,
ΔT_t, ΔT_c = the change in temperature from ambient conditions for the tube and tubesheet respectively.

During normal operation the temperature of the tube and tubesheet are effectively identical to within a very short distance from the top of the tubesheet and the individual changes in temperature can usually be replaced by ΔT_t, thus,

$$\delta = \left(\alpha_t - \alpha_c\right)\Delta T_t \, r_{to}\,. \tag{7}$$

The change in contact pressure due to the increase in temperature relative to ambient conditions, P_T, is given by,

a,c,e

[ ] (8)

Likewise, the same equation can be used to calculate the reduction in contact pressure resulting from a postulated reduction the temperature of the tube during a postulated SLB event.

The net contact pressure, P_C, between the tube and the tubesheet during operation or accident conditions is given by,

Net Contact Pressure $P_C = P_0 + P_P + P_T - P_B$ (9)

where P_B is the loss of contact pressure due to dilation of the tubesheet holes, P_0 is the installation preload, P_P is the pressure induced load, and P_T is the thermal induced contact load. There is one additional term that could be considered as increasing the contact pressure. When the temperature increases the tube expands more in the axial direction than the tubesheet. This is resisted by the frictional interface between the tube and the tubesheet and a compressive stress is induced in the tube. This in turn results in a Poisson expansion of the tube radius, increasing the interface pressure. The effect is not considered to be significant and is essentially ignored by the analysis.

9.1.3 Analysis

From the preceding discussions it is apparent that the contact pressure during normal operation can be found by equating the total deformation of the outside radius of the tube, r_{to}, to the total deformation of the inside radius of the tubesheet hole, r_{ci}, where the net deformation of the outside of the tube, δ_{to}, is given by,

Tube Deformation $\delta_{to} = \alpha_t \Delta T_t r_{to} + P_p f_{toi}^c + P_s f_{too}^c + P_N f_{too}^o$ (10)

and the net deformation of the tubesheet hole, δ_{ci}, is given by,

TS Deformation $\delta_{ci} = \alpha_c \Delta T_c r_{ci} + P_s f_{cii}^o + \delta r_i + P_N f_{cii}^o$. (11)

The inclusion of the P_N terms assures compatibility and the two net deformations must be equal. It can usually be assumed that the secondary fluid pressure does not penetrate the tubesheet hole and the terms involving P_s may be ignored. All of the terms except for the final contact pressure, P_N, are known and the tubesheet bow term, δ_{ri}, is found from the finite element model analysis of the tubesheet. The total contact pressure during operation is then found as P_N plus P_c, the installation contact pressure. For postulated SLB conditions the solution is obtained from,

$$\alpha_t \Delta T_t r_{to} + P_p f_{toi}^c + P_N f_{too}^o = \alpha_c \Delta T_c r_{ci} + \delta r_i + P_N f_{cii}^o, \quad (12)$$

or, the total contact pressure during a postulated SLB event is given by,

$$\text{SLB Contact Pres.} \quad P_T = P_c + \frac{\alpha_t \Delta T_t r_{to} - \alpha_c \Delta T_c r_{ci} + P_p f^c_{toi} - \delta r_i}{f^o_{cii} - f^o_{too}}, \tag{13}$$

where $r_{to} = r_{ci}$. A similar expression with more terms is used to obtain the contact pressure during normal operation. The denominator of the above equation is referred to as the tube-to-tubesheet influence coefficient because it related deformations associated with the interfacing components to the interface pressure. The influence coefficient for Westinghouse Model F SG tubes is calculated using the information tabulated in Table 9-1 as $3.33 \cdot 10^{-6}$ psi/inch.

By taking partial derivatives with respect to the various terms on the right the rate of change of the contact pressure as a function of changes in those parameters can be easily calculated. For example, the rate of change of the contact pressure with the internal pressure in the tube is simply,

$$\frac{\Delta P_N}{\Delta P_p} = \frac{f^c_{toi}}{f^o_{cii} - f^o_{too}}. \tag{14}$$

Thus, the rate of change of contact pressure with internal pressure in the tube is 0.564 psi/psi. Likewise, the rate of change of the contact pressure with change in the tube temperature or tubesheet temperature is given by,

$$\frac{\Delta P_N}{\Delta T_t} = \frac{\alpha_t r_{to}}{f^o_{cii} - f^o_{too}} \text{ and } \frac{\Delta P_N}{\Delta T_c} = -\frac{\alpha_c r_{ci}}{f^o_{cii} - f^o_{too}}, \tag{15}$$

respectively. Again using the values in Table 9-1, the rate of change of contact pressure with tube temperature is 18.3 psi/°F if there is no increase in tubesheet temperature. The corresponding change with an increase in tubesheet temperature without an increase in tube temperature is –17.36 psi/°F leave a net increase in contact pressure of 0.94 psi/°F with a uniform increase in temperature of the tube and the tubesheet.

Finally, the rate of change of contact pressure with tubesheet bow is calculated as,

$$\frac{\Delta P_N}{\Delta \delta r_{ci}} = \frac{1}{f^o_{cii} - f^o_{too}}. \tag{16}$$

The effect of the dilation associated with the tubesheet bow can be calculated using the information tabulated in Table 9-1. For each 0.1 mil of diameter dilation the interface pressure is reduced on the order of 380 psi. A summary of all of the contact pressure influence factors is provided in Table 9-2. A summary of tubesheet bow induced hole dilation values is provided in Table 9-3.

9.1.4 Conclusions

Although the study was completed for a Model F SG, the results listed in Table 9-3 indicate that the effect of tubesheet bow can result in a significant average decrease in the contact pressure during postulated accident conditions above the neutral plane. However, for the most severe case in one plant, in tube R18C77, the diametral change at the worst case location is less than 0.2 mils at the H* depth during postulated accident conditions. This same type of result would be expected to be the case for the Model 44F steam generators in Turkey Point Units 3 and 4. Below the neutral plane, tubesheet bow is shown not to result in any tube dilation thus supporting the NRC staff conclusion that:

> "Given the neutral axis to be at approximately the mid-point of the tubesheet thickness (i.e., 10.5 inches below the TTS to 17 inches below the TTS), tubesheet bore dilation effects would be expected to further tighten the joint from 10 inches below the TTS to 17 inches below the TTS which would be the lower limit of the proposed tubesheet region inspection zone. Combined with the effects of the tube joint tightening associated with the radial differential thermal expansion and primary pressure inside the tube, contact pressure over at least a 6.5 inch distance should be considerably higher than the contact pressure simulated in the above mentioned pullout tests."

9.2 JOINT LEAKAGE INTEGRITY DISCUSSION

As noted in Section 4.2, "Joint Leakage Integrity," of Reference 34, the NRC staff reviewed the qualitative arguments developed by Westinghouse regarding the conservatism of the conclusion that a minimum 17 inch engagement length ensures that leakage during a main steam line break (MSLB) will not exceed two times the observed leakage during normal operation. The NRC staff reviewed the qualitative arguments developed by Westinghouse regarding the conservatism of the "bellwether approach", but the NRC staff's depth of review did not permit it to credit Westinghouse's insights from leak test data that leak flow resistance is more sensitive to changes in joint contact pressure as contact pressure increases due to the log normal nature of the relationship. The staff was still able to conclude that there should be no significant reduction in leakage resistance when going from normal operating to accident conditions.

The basis for the Westinghouse conclusion that flow resistance varies as a log normal linear function of joint contact pressure is provided in detail below. The data from the worst case tube in a comparative study analytically supports the determination that there is at least an eight inch zone in the upper 17 inches of the tubesheet where there is an increase in joint contact pressure due to a higher primary pressure inside the tube and changes in tubesheet bore dilation along the length of the tubes. The NRC concurs that the factor of 2 increase in leak rate as an upper bound by Westinghouse is reasonable given the stated premise that the flow resistance between the tube and the tubesheet remains unchanged between normal operating and accident pressure differential. The NRC staff notes in Reference 4 that the assumed linear relationship between leak rate and differential pressure is conservative relative to alternative models such as the Bernoulli or orifice models, which assumes leak rate to be proportional to the square root of the differential pressure.

The comparative study supports the NRC staff conclusion that "considering the higher pressure loading when going from normal operating to accident conditions, Westinghouse estimates that contact pressures, and, thus, leak flow resistance, always increases over at least an 8 inch distance above 17 inches below the top of the tubesheet appears reasonable to the NRC Staff."

9.2.1 Loss Coefficient Contact Pressure Correlation

For subsequent analyses, the loss coefficient of the flow through the tube-to-tubesheet crevice must be determined as a function of the contact pressure between the tube and tubesheet. The plot of loss coefficient versus contact pressure for the Model D5 and Model F steam generators is provided in Section 6.3 of this report.

Since the Model D5, Model F and Model 44F steam generators have similar geometry along the tube-to-tubesheet crevice path, the Model D5 loss coefficients that were previously calculated can be used as applicable loss coefficients for the Model 44F steam generators. However, the Model D5 steam generator tubes have an outer diameter of 0.75 inch while the Model 44F steam generator tubes have an outer diameter of 0.875 inch. Therefore, in order to apply the Model D5 loss coefficients (which includes normalized Model F test results) to the Model 44F steam generators, the Model D5 loss coefficients must be multiplied by the ratio 0.750/0.875, which is the ratio of the Model D5 SG tube circumference to the Model 44F steam generator circumference. By applying the aforementioned scaling factor to the Model D5 loss coefficients, the results obtained are considered to be the loss coefficients that would have been obtained during the Model D5 testing if the Model D5 steam generators had tubes with an 0.875 inch outer diameter rather than 0.750 inch outer diameter.

A new linear regression and an uncertainty analysis was performed for the Model 44F steam generator. Figure 9-4 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 approximated by the following log-linear relation,

$$Log_{10}(K) = A\sigma_c + B \tag{17}$$

where A = slope of log-linear regression trendline,
B = y-intercept of log-linear regression trendline.

Therefore, the log-linear fit to the scaled Model 44F loss coefficient data follows the equation

$$Log_{10}(K) = (2.14)\sigma_c + 12.186 \qquad \text{(Reference 32)} \tag{18}$$

a,c,e

Table 9-1. Radial Flexibilities Times Elastic Modulus (in./psi)				

a,c,e

Table 9-2. Contact Pressure Influence Factors for Model F SG Tubes at 600°F	

Table 9-3. Tubesheet Hole Diametral Dilation for R18C77

a,c,e

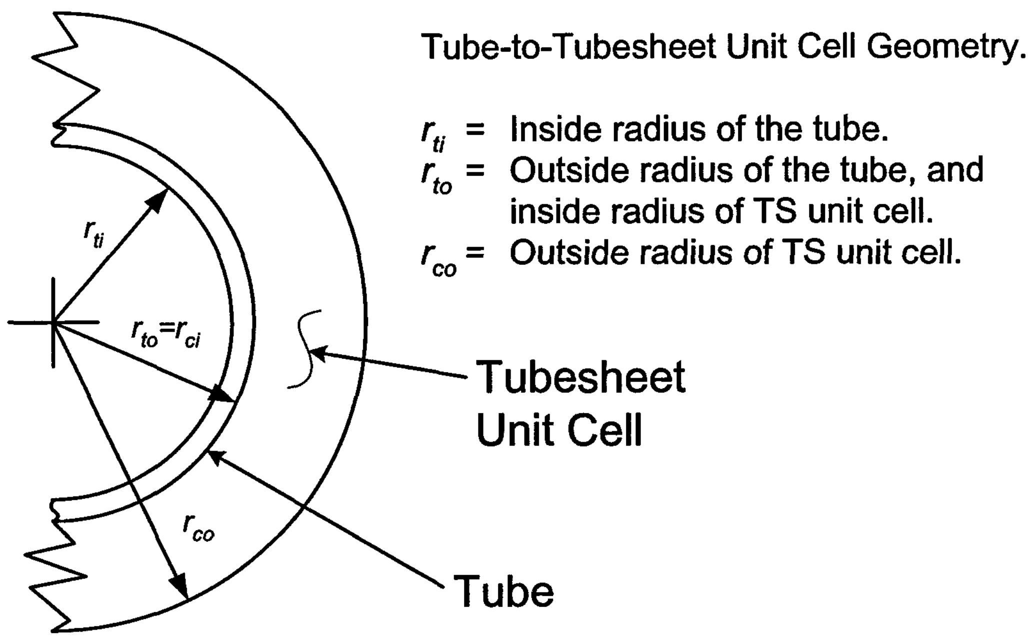

Figure 9-1. Geometry of the Tube-to-Tubesheet Interface

a,c,e

Figure 9-2. Model for Initial Contact Pressure

a,c,e

Figure 9-3. Determination of Contact Pressure, Normal or Accident Operation

(As illustrated, the bow does not result in a loss of contact, however, there are situations where the bow is sufficient to result in a loss of contact between the tube and the tubesheet at the top of the tubesheet.)

a,c,e

Figure 9-4. Scaled Flow Resistance Curve for Model 44F Steam Generators

10.0 CONCLUSIONS

The evaluation in Section 8.0 of this report provides a technical basis for bounding the potential leak rate from tube degradation of any magnitude below 17 inches from the top of the tubesheet as no more than twice the leak rate during normal operation. The region below 17 inches from the top of the tubesheet includes the tack expansion and the tube-to-tubesheet welds. The conclusions apply to any postulated indications in the tack expansion region and in the tube-to-tubesheet welds.

The evaluations performed as reported herein have demonstrated that:

1. There is no structural integrity concern associated with tube or tube weld cracking of any extent provided it occurs below the H* distance as reported in Section 7.0 of this report. The pullout resistance of the tubes has been demonstrated for axial forces associated with 3 times the normal operating differential pressure and 1.4 times differential pressure associated with the most severe postulated accident.
2. The leak rate for indications below a depth of 17 inches from the top of the tubesheet would be conservatively bounded by twice the leak rate that is present during normal operation of the plant regardless of tube location in the bundle. This is initially apparent from comparison of the contact pressures from the finite element analyses over the full range of radii from the center of the tubesheet, and ignores any increase in the leak rate resistance due to the contact pressure changes and associated tightening of the crack flanks. The expectation that this would be the case was confirmed by the detailed analysis of the relative leak rates of Section 8.0.

It has been demonstrated that a relocation of the pressure boundary to 17 inches below the top of the tubesheet is acceptable from both a structural and leak rate considerations on a permanent basis. The prior conclusions rely on the inherent strength and leak rate resistance of the hydraulically expanded tube-to-tubesheet joint, a consideration not permitted for the original design of the SG. Thus, elimination of a portion of the tube (i.e., a portion of the pressure boundary) within the tubesheet from the inservice inspection requirement constitutes a change in the pressure boundary. Similar considerations for tube indications require NRC staff approval of a license amendment.

With regard to the preparation of a significant hazards determination, the results of the testing and analyses demonstrate that the relocation of the pressure boundary to a depth of 17 inches form the top of the tubesheet does not lead to an increase in the probability or consequences of the postulated limiting accident conditions because the margins inherent in the original design basis are maintained and the expected leak rate during the postulated accident is not expected to increase beyond the plant specific limit. In addition, the relocation of the pressure boundary does not crate the potential for a new or departure from the previously evaluated accident events. Finally, since the margins inherent in the original design bases are maintained, no significant reduction in the margin of safety would be expected.

WCAP-16506-NP

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ENCLOSURE 7

Westinghouse letter CAW-06-2092, Affidavit, Proprietary Information Notice and Copyright Notice

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Our ref: CAW-06-2092

January 26, 2006

APPLICATION FOR WITHHOLDING PROPRIETARY
INFORMATION FROM PUBLIC DISCLOSURE

Subject: WCAP-16506-P, "Steam Generator Tube Alternate Repair Criteria for the Portion of the Tube Within the Tubesheet at Turkey Point Units 3 and 4," dated December 2005 (Proprietary)

The proprietary information for which withholding is being requested in the above-referenced report is further identified in Affidavit CAW-06-2092 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 Florida Power and Light Group, Inc.

Correspondence with respect to the proprietary aspects of the application for withholding or the Westinghouse affidavit should reference this letter, CAW-06-2092, and should be addressed to B. F. Maurer, Acting Manager, Regulatory Compliance and Plant Licensing, Westinghouse Electric Company LLC, P.O. Box 355, Pittsburgh, Pennsylvania 15230-0355.

Very truly yours,

B. F. Maurer, Acting Manager
Regulatory Compliance and Plant Licensing

Enclosures

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L. Feizollahi

bcc: B. F. Maurer (ECE 4-7A) 1L
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J. P. Molkenthin, Windsor

CAW-06-2092

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:

BF Maurer

B. F. Maurer, Acting Manager
Regulatory Compliance and Plant Licensing

Sworn to and subscribed
before me this 26th day
of January, 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 Acting Manager, Regulatory Compliance and Plant Licensing, in 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-16506-P, "Steam Generator Tube Alternate Repair Criteria for the Portion of the Tube Within the Tubesheet at Turkey Point Units 3 and 4," dated December 2005 (Proprietary). The information is provided in support of a submittal to the Commission, being transmitted by Florida Power and Light Group, Inc. and Application for Withholding Proprietary Information from Public Disclosure, to the Document Control Desk. The proprietary information as submitted for use by Westinghouse for Turkey Point Units 3 and 4 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 Turkey Point Units 3 and 4 steam generators.

(b) Provide a primary-to-secondary side leakage evaluation for Turkey Point Units 3 and 4 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).

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