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Limited Inspection of the Steam Generator Tube Portion Within the Tubesheet at Seabrook Generating Station

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Abstract

Nondestructive examination indications of primary water stress corrosion cracking were found in the Alloy 600 thermally treated Westinghouse Model D5 steam generator tubes at the Catawba 2 nuclear power plant in the fall of 2004. Most of the indications were located in the tube-to-tubesheet welds with a few of the indications being reported as extending into the parent tube. In addition, a small number of tubes were reported with indications about 3/4 inch above the bottom of the tube, 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 predominantly 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, which has Westinghouse Model F SGs. Based on recent requirements interpretations published by the NRC staff in Generic Letter 2004-01 and Information Notice 2005-09, Florida Power and Light (FP&L) requested that a recommendation be developed for examination of the Westinghouse Model F steam generator tubesheet regions at the Seabrook Generating Station. 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 the structural and leak rate integrity of the primary-to-secondary pressure boundary is unaffected by degradation of any level below a depth of 17 inches from the top of the 21 inch (nominal) thick tubesheet or the tube end welds because the tube-to-tubesheet hydraulically expanded joints make it extremely unlikely that any operating or faulted condition loads are applied to the tube tack expanded region or the tube welds. Internal tube bulges, i.e., within the tubesheet, were created in a number of tubes as an artifact of the manufacturing process. The possibility of degradation at these locations exists based on the reported degradation at Catawba 2 and Vogtle 1. A recommendation is made for examination of a sample of the Seabrook tubes to a depth of 17 inches below the top of the tubesheet based on the use of a bounding leak rate evaluation and the application of a structural analysis of the tube-to-tubesheet joint first documented in WCAP-16053 and repeated in Appendix A of this report. Application of the bounding leak rate and structural analysis approaches supporting this conclusion requires the approval of the NRC staff through a license amendment because it is based on a redefinition of the primary-to-secondary pressure boundary relative to the original design of the plant.

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1.0 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 1, 2 and 3. The SGs at the Catawba 2 plant are type Westinghouse Model D5 with 3/4 inch nominal outside diameter (OD), thermally treated Alloy 600 tubes (A600TT). 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, with a small number of those indications extending into the tubes. The spatial distribution by row and column number is shown on Figure 1 for SG A, Figure 2 for SG B, and Figure 3 for SG D at Catawba; there were no indications in SG C. Circumferential indications were reported in the spring of 2005 in two SG tubes, one tube had two indications, at the Vogtle Unit 1 plant operated by the Southern Nuclear Operating Company, Reference 4. The Vogtle SGs are of the Westinghouse Model F design like those at the Seabrook Generating Station, hereinafter referred to simply as Seabrook, with nominal 11/16 inch diameter A600TT tubes. Similar indications have not been reported at the other plant sites with Model D5 SGs, nor have any been reported in other Model F SGs. However, except for Braidwood 2 and Wolf Creek, who tested a significant sample of tube expansion regions using the RPC (rotating probe coil), it is believed that no RPC inspection of the tube region in the vicinity of the tack expansions or the tube-to-tubesheet welds has been performed. It is likely that only bobbin coil eddy current test (ECT) and visual examination using SG bowl cameras have been performed in the vicinity of the tube-to-tubesheet weld. In other words, ECT inspections using techniques capable of detecting circumferential cracking within the tubesheet have not been used in areas significantly below the top-of-tubesheet expansion transition region, typically limited to a depth of 3 inches from the top of tubesheet or the tube transition region. The Seabrook practice has been consistent with the industry practice to test a sample of tubes to 3 inches below the top of the tubesheet. Thus, there is a potential for tube indications within the tubesheet region similar to those reported at Catawba 2 and Vogtle 1 to be reported in the Seabrook SGs if similar inspections were to be performed during future inspections of the SGs.

The Model F SGs were fabricated in the 1979 through 1988 timeframe using similar manufacturing processes with a few exceptions. For example, relative to Catawba 2 , the fabrication technique used for the installation of the SG tubes at Vogtle 1 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 because a different process for effecting the tack expansions was adopted prior to the time of the fabrication of the Vogtle 1 SGs. The same statement is true with regard to the tack expansion region in the Seabrook SGs since they were fabricated after the Catawba 2 SGs and in the same year as the Vogtle 1 SGs, using the same reduced stress tack expansion process.

With regard to the tack expansion region of the tube and the tube end welds, the recommendation is to not perform any specific inspection of these regions of the SG tubes at the Seabrook plant site. This recommendation is not part of an attempt to license the H* methodology as described in Reference 5 for application to the tubes in the Seabrook SGs; however, the structural analysis of the tube and the tubesheet documented in that reference is valid for use in supporting the application of an independent leakage evaluation methodology based on the change in contact pressure between the tube and the tubesheet between normal operation and postulated accident conditions. Moreover, in order to address potential uncertainties associated with the determination of specific leak rates, Seabrook will increase the effective depth for RPC inspection of the tubes to 17 inches from the top of tubesheet (TTS), but may bias the sample to the region of greater structural significance less than 17 inches from the TTS. This allows the use of the newly developed leak rate methodology with regard to the potential for indications in the tack expansion transition or tube weld since excluded potential degradation regions would be limited to the lower 4.26 inches of the tube in the nominally 21.26 inch thick tubesheet, which is well below the mid-plane of the tubesheet. As described in Section 6.1 of this report, the potential leakage due to degradation below 17 inches from the TTS would clearly be below allowable accident limits.

The findings in the Catawba 2 and Vogtle 1 SG tubes present three distinct issues with regard to the SG tubes at the Seabrook plant:

1) indications in internal bulges or expansion anomalies 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 7, and regulatory issues, Reference 8, and b) provide analysis support for technical arguments to limit inspection of the tubesheet region to an area above which degradation could result in potentially not meeting the SG performance criteria, i.e., the depths specified in Reference 5 or 17 inches as recommended herein. The application of any justification to limit the inspection and repair extent of the tubes requires a redefinition of the primary-to-secondary pressure boundary for plants with hydraulically expanded tube-to-tubesheet joints for which a license amendment must be granted by the NRC for implementation. In order to limit the extent of the inspection in future inspections of the Seabrook SGs, a technical specification, a.k.a. the TS, amendment is being sought. This report was prepared to facilitate the approval of a modification of the H* criteria to justify the RPC exclusion zone to the portion of the tube below 17 inches from the top of the tubesheet and provide the necessary information for a NRC staff review of the technical basis for that request.

It should be specifically noted that although the terminology of "H*" is used extensively throughout this document. Seabrook is not attempting to license H*, but to use structural analysis results and experimental data trends extracted from the existing H* report, Reference 5, in order to support justification of a limited tube inspection extent from the top of the hot leg side of the tubesheet to a depth of 17 inches. Therefore, degradation below the top 17 inches of tube within the tubesheet can remain in service since it is demonstrated herein to be not safety significant.

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 9, and draft RG 1.121, Reference 11. The bases for the performance criteria are the demonstration of both structural and leakage integrity during normal operation and postulated accident conditions. The Reference 5 report included documentation of structural analyses regarding the efficacy of the tube-to-tubesheet joint, and leak rate analyses based on empirical data and computer code modeling of the leakage from tubes postulated to be cracked 100% throughwall within the tubesheet. The structural model was based on standard analysis techniques and finite element models as used for the original design of the SGs and documented in numerous submittals for the application of criteria to deal with tube indications within the tubesheet of other models of Westinghouse designed SGs with tube-to-tubesheet joints fabricated by other techniques, e.g., explosive expansion. The structural analysis of the Seabrook SG tube-to-tubesheet joints is provided in Appendix A to this report. The content is the same as that in Reference 5 and permits for the review of the structural analysis to be performed independent of the Reference 5 information.

All full depth expanded tube-to-tubesheet joints in Westinghouse designed SGs have a residual radial preload between the tube and the tubesheet. Early vintage SGs involved hard rolling which resulted in the largest magnitude of the residual interface pressure. Hard rolling was replaced by explosive expansion which resulted in a reduced magnitude of the residual interface pressure. Finally, hydraulic expansion replaced explosive expansion for the installation of SG tubes, resulting in a further reduction in the residual interface pressure. In general, it was found that the leak rate through the joints in hard rolled tubes, if any, is insignificant. Testing demonstrated that the leak rate resistance of explosively expanded tubes was not as great and prediction methods based on empirical data to support theoretical models were developed to deal with the potential for leakage. The same approach was followed to develop a prediction methodology for hydraulically expanded tubes. However, the model has been under review 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 its review could be time consuming since it has not been previously reviewed by the NRC staff. An alternative approach was developed for application at Seabrook based on engineering expectations of potential differences in the leak rate between normal operation and postulated accident conditions based on a first principles approach to the engineering. There are no technical reasons why the use of the alternate methodology should be limited to a single application.

A summary of the evaluation is provided in Section 2.0 of this report. The historical background and design requirements for the tube-to-tubesheet joint are discussed in Sections 3.0 and 4.0 respectively, a summary of the conclusions from the structural analysis of the joint is provided in Section 5.0, the leak rate analysis in Section 6.0, dispositioning of cracked tubes inadvertently found below the inspection distance is discussed in Section 7.0, conclusions from the structural and leak rate evaluations are provided in Section 8.0, and recommended tube inspection plans are contained in Section 9.0.

2.0 Summary Discussion

Evaluations were performed to assess the need for special purpose NDE probe examinations, e.g., RPC, of the SG tubes region within the tubesheet at the Seabrook power plant. The conclusions from the evaluation are that a sample of the population of tube bulges and over expansions, designated as BLG and OXP respectively for ECT purposes, in each SG could be performed to at least the minimum depths specified in Reference 5, identified as H* in that reference, to ensure structural integrity. SEABROOK will perform sampling RPC inspections of the BLG and OXP locations within the region to 17 inches below the top of the tubesheet for the Fall-2006 inspection (OR11) and subsequent inspections. The sample size is based on the population of such signals to a depth of at least 17 inches into the tubesheet for each SG. If indications are confirmed during the inspection of the sample, the inspection scope will be expanded to include the entire population of BLG and OXP signals to a depth of 17 inches for the affected SG and a 20% sample of each of the unaffected SGs. The leakage performance requirement, in addition to the structural requirements, is met because it has been demonstrated that a bounding value of the leak rate during a postulated SLB event can be estimated from the leak rate during normal operation.

It is noted that the above inspection plan excludes the region of the tube referred to as the tack expansion or the tack expansion transition. In addition, consideration was given to the need to perform inspections of the tube-to-tubesheet weld in spite of the fact that the weld is specifically not part of the tube in the sense of the plant technical specification, see Reference 2. With regard to the latter two regions of the primary-to-secondary pressure boundary in accord with the original design of the SGs, it is concluded that there is no need to inspect either the tack expansion, its transition, or the tube-to tubesheet welds for degradation because the tube in these regions has been shown to meet structural and leak rate criteria regardless of the level of degradation. Furthermore, it could also be concluded that for some of the tubes, depending on radial location in the tubesheet, there is not a need to inspect the region of the tube below the neutral plane of the tubesheet, roughly 11 inches below the top. The results from the evaluations performed as described herein demonstrate that the inspection of the tube within a nominal 4.26 inches of the tube-to-tubesheet weld and of the weld itself is not necessary for structural adequacy of the SG during normal operation or during postulated faulted conditions, nor for the demonstration of compliance with leak rate limits during postulated faulted events.

In summary:

- WCAP-16053, Reference 5, notes that the structural integrity requirements of NEI 97-06, Reference 9, and draft RG 1.121, Reference 11, are met by sound tube engagement lengths ranging from 3.29 to 8.50 inches from the top of the tubesheet, thus the region of the tube below those elevations, including the tube-to-tubesheet weld is not needed for structural integrity during normal operation or accident conditions.
- NEI 97-06, Reference 9, defines the tube as extending from the tube-to-tubesheet weld at the tube inlet to the tube-to-tubesheet weld at the tube outlet, but specifically excludes the tube-to-tubesheet weld from the definition of the tube. The acceptance of the definition by the NRC staff was recorded in the Federal Register on March 2, 2005, Reference 12.

- The welds were originally designed and analyzed as primary pressure boundary in accordance with the requirements of Section III of the 1971 edition of the ASME (American Society of Mechanical Engineers) Boiler & Pressure Vessel Code, Summer 1973 Addenda, Reference 7. The analysis of the weld is documented in Reference 13 for the Seabrook SGs. The typical as-fabricated and the as-analyzed weld configurations are illustrated on Figure 4.
- Section XI of the ASME Code, Reference 16 (1971) through 17 (2004), deals with the inservice inspection of nuclear power plant components. The ASME Code specifically recognizes that the SG tubes are under the purview of the NRC through the implementation of the requirements of the Technical Specifications as part of the plant operating license.

The hydraulically expanded tube-to-tubesheet joints in Model F SGs are not leak-tight and considerations were also made with regard to the potential for primary-to-secondary leakage during postulated faulted conditions. Two evaluation approaches were considered, one based on the leak rate during normal operation relative to that during postulated accident conditions and the second based on leak rate prediction analyses documented in WCAP-16053, Reference 5, prepared for the purpose of identifying a structurally based depth for RPC inspection in the event that circumferential cracking below the top of the tubesheet was postulated to be present and estimating the leak rate that could be expected from a conservatively based prediction of the number of non-detected indications potentially present. Owing to the potential for a lengthy review process for the second approach, the method was not pursued for evaluation and implementation.

The leak rate during postulated accident conditions would be expected to be less than that during normal operation for indications near the bottom of the tubesheet (including indications in the tube end welds) based on the observation that while the driving pressure increases by about a factor of two, the flow resistance increase associated with an increase in the tube-to-tubesheet contact pressure can be up to a factor of 3, Reference 5. While such a decrease is rationally expected, the postulated accident leak rate could conservatively be taken to be bounded by twice the normal operating leak rate if the increase in contact pressure is ignored. Since normal operating leakage is limited to less than 0.1 gpm[1] (150 gpd, per Reference 9), the attendant accident condition leak rate, assuming all leakage to be from lower tubesheet indications, would be bounded by 0.2 gpm[2], which is less than the 0.347 gpm limit specified in Reference 10. Therefore, the leak rate under normal operating conditions could exceed its allowed value before the accident condition leak rate would be expected to exceed its allowed value. This approach is considered to be an application of the "bellwether principle." This assessment also envelopes postulated circumferential cracking of the tube or the tube-to-tubesheet weld that is 100% deep by 360° in extent because it is based on the premise that no weld is present.

Based on the information summarized above, no inspection of the tube-to-tubesheet welds, tack expansion region or bulges below the distance determined to have the potential for safety significance as specified in Reference 5, i.e., the H* depths, would be considered to be necessary to assure

[1] The Seabrook plant administrative limit for normal operating leakage is 75 gpd.
[2] The expected leak rate would decrease significantly if the attendant increase in contact pressure and resistance to leak were included.

compliance with the structural and primary-to-secondary leak rate requirements for the SGs. In addition, based on the results from consideration of application of the bellwether principle regarding potential leakage during postulated accident conditions, the planned inspection to a depth of 17 inches below the top of the tubesheet is conservative and justified.

The selection of a depth of 17 inches obviates the need to consider the location of the tube expansion transition below the TTS, usually bounded by a length of about 0.3 inches. For structural purposes, the value of 17 inches greatly exceeds the engagement lengths determined from the analysis documented in Appendix A. The application of the bellwether approach to the leak rate analysis as described in Section 6.1 negates the need to consider specific distances from the TTS and relies only on the magnitude of the contact pressure in the vicinity of the tube above 17 inches below the TTS.

3.0 Historical Background Regarding Tube Indications in the Tubesheet

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 hard rolling 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 aggressive with regard to metalworking at the inside surface of the tube and would be expected to lead to higher residual surface stresses. The tube-to-tubesheet weld was then performed to create the ASME Code pressure boundary between the tube and the tubesheet.[3] During the manufacture of the Seabrook SGs, the tubing sets were delivered between April 1980 and July 1980; thus it is concluded that the more benign urethane plug tack expansion process was used during fabrication of the Seabrook SGs, instead of the rolling process used for earlier Model F SGs.

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 16. 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 11. 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 19. The W* criteria were first applied to operating SGs in 1999 based on a generic evaluation for Model 51 SGs, Reference 20, and the subsequent safety evaluation by the NRC staff, Reference 21. However, the

[3] The actual weld is between the Alloy 600 tube and weld buttering on the bottom of the carbon steel tubesheet.

required engagement length to meet structural and leakage requirements was on the order of 4 to 6 inches because the 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 usually shorter than, that needed to meet the leakage integrity requirements.

The post-weld expansion of the tube into the tubesheet in the Seabrook 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 Seabrook SGs, on the structural and leakage integrity of the joint, Reference 22. 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 9 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.

4.0 Design Requirements for the Tube-to-Tubesheet Joint Region

This section provides a review of the applicable design and analysis requirements, including the ASME Code pre-service design requirements of Section III and the operational/maintenance requirements of Section XI. The following is the Westinghouse interpretation of the applicable analysis requirements and criteria for the condition of TEW cracking. Recommendations that include code requirements and the USNRC position as expressed in References 8 and 9. Reference 8 notes that:

> *"In accordance with Section III of the Code, the original design basis pressure boundary for the tube-to-tubesheet joint included the tube and tubesheet extending down to and including the tube-to-tubesheet weld. The criteria of Section III of the ASME Code constitute the "method of evaluation" for the design basis. These criteria provide a sufficient basis for evaluating the structural and leakage integrity of the original design basis joint. However, the criteria of Section III do not provide a sufficient basis by themselves for evaluating the structural and leakage integrity of a mechanical expansion joint consisting of a tube expanded against the tubesheet over some minimum embedment distance. If a licensee is redefining the design basis pressure boundary and is using a different method of evaluation to demonstrate the structural and leakage integrity of the revised pressure boundary, an analysis under 10 CFR 50.59 would determine whether a license amendment is required."*

The industry definition of Steam Generator Tubing excludes the tube-end weld from the pressure boundary as noted in NEI 97-06 (Reference 9):

> *"Steam generator tubing refers to the entire length of the tube, including the tube wall and any repairs to it, between the tube-to-tube sheet weld at the tube inlet and*

the tube-to-tube sheet weld at the tube outlet. The tube-to-tube sheet weld is not considered part of the tube."

The NRC has indicated its concurrence with this definition; see, for example, Reference 12. In summary, from a non-technical viewpoint, no specific inspection of the tube-end welds would be required because:

1. The industry definition of the tube excludes the tube-end weld,
2. The ASME Code defers the judgment regarding the redefined pressure boundary to the licensing authority under 10CFR50.59,
3. The NRC has accepted this definition; therefore, by inference, may not consider cracked welds to be a safety issue on a level with that of cracked tubes, and
4. There is no qualified technique that can realistically be applied to determine if the tube-end welds are cracked.

However, based on the discussion of Information Notice 2005-09, Reference 2, it is clear that the NRC staff has concluded that "the findings at Catawba illustrate the importance of inspecting the parent tube adjacent to the weld and the weld itself for degradation." The technical considerations documented herein obviate the need for consideration of any and all non-technical arguments.

5.0 Structural Analysis of Tube-to-Tubesheet Joint

This section summarizes the structural aspects and analysis of the entire tube-to-tubesheet joint region, the details of which are provided in Appendix A. The tube end weld was originally designed as a pressure boundary structural element in accordance with the requirements of Section III of the ASME Boiler and Pressure Vessel Code, Reference 7. 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 Seabrook Model F SGs and the results were reported in Westinghouse report WCAP-16053, Reference 5. Typical Model F hydraulic expansion joints with lengths comparable to those being proposed in Reference 5 for limiting RPC examination were tested for pullout resistance strength at temperatures ranging from 70 to 600°F. Finite element models were developed to evaluate the effects of temperature and primary-to-secondary pressure on the tube-to-tubesheet interface loads. The test results and the results of the finite element evaluations demonstrate that engagement lengths of approximately 3 to 8.6 inches are sufficient to equilibrate the axial loads resulting from consideration of 3 times the normal operating pressure difference and 1.4 times the limiting accident condition pressure difference. The variation in required engagement length is a function of tube location, i.e., row and column; the required engagement length decreases with increasing distance 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 a short distance below the mid-plane, because of the effect of the tensile

membrane stress from the pressure loading. The amount of dilation is a maximum very near the radial center of the tubesheet (restricted by the divider plate) and diminishes 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[4]. In conclusion, the weld is not necessary because of 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, including postulated loss of coolant accidents (LOCA). Therefore, inspection of the tube below the H* distance including the tube-to-tubesheet weld is not technically necessary. 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 nearly 180° by 100% deep would have sufficient strength to meet the nominal ASME Code structural requirements. See Reference 13 for the margins of safety and Reference 11 for the applicable requirements in RG 1.121.

An examination of Table A.6 through Table A.10 provides information that the holding power of the tube-to-tubesheet joint in the vicinity of the maximum inspection depth of 17 inches is much greater than at the top of the tubesheet in the range of the originally developed H* of Reference 5. Note that the radii reported in these tables were picked to conservatively represent entire radial zones of consideration as defined on Figure 5 (taken from Reference 5). Four zones were originally identified for which to specify individual H* values to permit minimizing the inspection depth as a function of location in the plane of the tubesheet. For example, Zone D has a maximum radius of 12.0 inches. In order to establish H* values that were conservative throughout the zone, the tube location for which the analysis results were most severe above the neutral axis were reported, i.e., those values calculated for a tube at a radius of 4.02 inches, but conservatively applied out to the tube radius of 12.0 inches. The values are everywhere conservative above the neutral surface of the tubesheet for tubes in Zone D, see Figure 5. Likewise, for tubes in Zone B, under the heading 48.613 inches, where the basis for the calculation was a tube at a radius of 30.193 inches. The same approach was used for the predictions reported for Zones C and A. 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 4.9 inch range from 12 to 16.9 inches below the top of the tubesheet is about 880 lbf per inch during normal operation, see Table A.6. The performance criterion for $3 \cdot \Delta P$ is met by the first 1.9 inches of engagement above 17 inches. At a radius of 58.3 inches the corresponding length of engagement needed is about 3.0 inches. The corresponding values for steam line break conditions are 1.09 and 1.85 inches at radii of 4.02 and 58.3 inches respectively. In other words, while a value of 8.5 inches was determined for H* from the top of the tubesheet, a length of 1.9 to 3.0 inches would be sufficient at the bottom of the inspection length for the $3 \cdot \Delta P$ performance criterion, where the latter value corresponds to a radius of 58.3 inches from the center of the tubesheet, the limiting location of Zone A.

[4] There is a small reversal of the bending stress beyond a radius of about 55 inches because the support ring prevents rotation and the hole dilation is at the bottom of the tubesheet.

6.0 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. Although the welds are not part of the tube per the technical specifications, consideration is given in deference to the discussions of the NRC staff in References 2 and 8. Consideration of the leak rate through 100% throughwall cracks in the SG tubes at locations below the top of the tubesheet was given extensively in Reference 5. The hydraulically expanded joint is not leak tight; therefore the leak rate is a function of the distance to the tip of the crack from the top of the tubesheet and the contact pressure between the tube and the tubesheet.

The approach to dealing with leakage in Reference 5 is based on counting the number of cracks present in the inspected region above a critical depth designated therein as H* in order to predict the distribution of cracks below H* and then estimating the leak rate from those cracks. A bounding distribution of cracks was proposed for initial application based on the number of cracks that were detected in the SGs at a plant where the tubes were made from Alloy 600 mill annealed (A600MA) material. The thermally treated tube material in the Seabrook SG tubes has been demonstrated experimentally to be much more resistant to PWSCC so the number of indications observed at that plant is expected to be bounding by a very significant margin at similar times of operation when adjusted for temperature. Furthermore, the distribution used as bounding was based on the number of indications present several years after the first indications had been observed, thus the distribution was more mature. It is important to note that the degradation reported in the Catawba 2 and Vogtle 1 SG tubes was bounded by the degradation extent specified for application by Reference 5. The methodology for estimating the leak rate from such indications as delineated in Reference 5 is grossly conservative in that it omits consideration of the operating characteristics of the plant with regard to primary-to-secondary leakage.

Although the methodology applies throughout the tubesheet, other considerations can be made with regard to assessing the reduction in the potential for leakage when the indications are below the neutral surface of the tubesheet, which is located slightly below the mid-plane because the primary-to-secondary pressure difference induces a membrane stress in addition to the bending stress. Both approaches are explained in the following sections; however, because of the major importance of the additional consideration, referred to as the bellwether approach, it is discussed first. It is noted that the application of the discussed methods requires approval from the NRC staff to change the Technical Specification prior to returning to service after the fall 2006 outage for Seabrook. 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 through removal of a tube section followed by destructive metallurgical examination at Catawba 2 or Vogtle 1.

6.1 The Bellwether Principle for Normal Operation to Steam Line Break Leak Rates

From an engineering expectation standpoint, if there is no meaningful primary-to-secondary leakage during normal operation, there should likewise be no meaningful leakage during postulated accident conditions from indications located below the mid-plane of the tubesheet. The rationale for this is based

on considerations regarding the deflection of the tubesheet with accompanying dilation and constriction of the tubesheet holes. In effect, the area presented as a leak path between the tube and tubesheet would not be expected to increase under postulated accident conditions and would really be expected to decrease for most of the SG tubes. During the development of the RPC inspection criteria of Reference 5, consideration was given regarding the potential for leak rate during normal operation to act as a "bellwether" or leading indicator with regard to the leak rate that could be expected during postulated accident conditions.

These results were not included in the final versions of the document because of concerns associated with the accuracy of the approach above the neutral plane of the tubesheet where the tube-to-tubesheet contact pressure would usually be expected to diminish during faulted conditions. For example, if it was intended to stop the RPC examination at a depth of 3 to 9 inches from the top of the tubesheet, then severe circumferential cracking would have been postulated to occur immediately below that depth and the potential leak rate as compared to that during normal operation estimated. The primary-to-secondary pressure difference during normal operation is on the order of 1200 to 1400 psi, while that during a postulated accident, e.g., steam line and feed line break, is on the order of 2560 to 2650 psi.[5] Above the neutral plane of the tubesheet the tube holes 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, leading to an expectation of an increase in the potential leak rate through the crevice. Estimating the change in leak rate as a function of the change in contact pressure under faulted conditions on a generic basis was expected to be problematic. However, below the neutral plane of the tubesheet, the tube holes diminish in size 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 constriction of the holes below the neutral plane permits definitive statements to be made with regard to the trend of the leak rate, hence, the bellwether principle.

Independent consideration of the effect of the tube-to-tubesheet contact pressure leads to similar conclusions with regard to the opening area of the cracks in the tubes, thus further restricting the leak rate beyond that through the interface between the tube and the tubesheet. In order to accept the concept of normal operation being a bellwether for the postulated accident leak rate for indications above the neutral plane of the tubesheet, the change in leak rate had to be quantified using a somewhat complex, but 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 because a diminution of the holes takes place during postulated accident conditions below the neutral plane relative to normal operation. For example, at a radius of approximately 30 inches from the center

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

of the SG, the contact pressure at the bottom of the tubesheet during normal operation is calculated to be about 2500 to 2790 psi[6], see the last contact pressure entry in the center columns of Table A.7 and Table A.6 respectively, while the contact pressure during a postulated steam line break would be on the order of 4500 psi at the bottom of the tubesheet, Table A.8, and during a postulated feed line break would be on the order of 5070 to 5125 psi at the bottom of the tubesheet, Table A.9 and Table A.10 respectively. (Note: The radii specified in the heading of the tables are the maximum values for the respective zones analyzed, hence the contact pressures in the center column correspond to the radius specified for the left column, etc. The leftmost column lists the contact pressure values for a radius of 4.02 inches, but is applied to a radius of 12.0 inches.) The analytical model for the flow through the crevice, the Darcy equation for flow through porous media, indicates that flow would be expected to be proportional to the differential pressure. Thus, a doubling of the leak rate could be predicted if the change in contact pressure between the tube and the tubesheet were ignored.

Examination of the nominal correlation on Figure 6 (Figure 6.1 of Reference 5) from test results obtained using deionized and deoxygenated water[7] indicates that the resistance to flow per unit length (the loss coefficient) would increase by a factor of about 16. However, that correlation was originally developed using results from tests performed at room temperature in order to obtain loss coefficient values for very low contact pressures. The regression analysis was repeated for this report based on considering only the results from the tests performed at a temperature of 600°F. Using the results from that analysis indicates the increase in the leak resistance would be a factor of 6 between SLB and Normal Operating (NOp) conditions. If the leak rate during normal operation was 0.1 gpm (150 gpd)[8], the postulated accident condition leak rate would be 0.2 gpm versus the allowable limit of 0.347 gpm when considering only the change in differential pressure. However, the estimate would be reduced to 0.06 gpm when the decrease in leak rate associated with an increase in contact pressure is included, i.e., about 60% of that during normal operation based on the factor of 6. This latter value is significantly less than the allowable limit during faulted conditions of 0.347 gpm at room temperature density. Even without including the effect of the change in contact pressure, the predicted leak rate would be significantly less than the allowable rate of 0.347 gpm.

A similar analysis, performed for Westinghouse Model D5 SGs (there are differences in tube size, pitch and number), Reference 15, reported an expected reduction in leak rate of a factor of 3 compared to the result for the Model F SGs. Regardless of the difference in the magnitude of the reduction factors, it is apparent that the inclusion of the increase in resistance to flow through the tube-to-tubesheet interface would have a meaningful effect on the expected leak rate during a postulated SLB event. The increased resistance to flow can result in a predicted value that is less than the normal operating value for a significant number of tubes in the bundle. The above argument considered indications located where the expectations associated with the bellwether principle would be a maximum, i.e., where the relative increase in contact pressure from normal to faulted conditions is a maximum. Thus, the conclusions of this section apply directly to indications in the tube somewhat near the bottom of the

[6] The change occurs as a result of considering various operating temperatures and pressures.

[7] Confirmed by testing to perform the same as primary water, Reference 14.

[8] The 150 gpd is the spike limit; the Seabrook plant administrative limit for continuous normal operating leakage is 75 gpd.

tubesheet, i.e., as a minimum to tube indications within a little more than 4 inches from the bottom of the tubesheet and to postulated indications in the tube-to-tubesheet welds.

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 A.6 through Table A.10 shows that the bellwether principle applies to a significant extent to all indications below the neutral plane of the tubesheet. At the central plane of the tubesheet the increase in contact pressure is more on the order of 47% 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 17 inches from the top of the tubesheet the contact pressure increases up to about 61% relative to that during normal operation. The flow resistance would be expected to increase by about 60%, thus the increase in driving pressure would be mostly offset by the increase in the resistance of the joint.

The numerical results from the finite element analyses are presented on Figure 7 at the elevation of the mid-plane of the tubesheet through Figure 10 at the bottom of the tubesheet. A comparison of the contact pressure during postulated SLB conditions relative to that during normal operation is provided for depths of 10.5, 12.6, 16.9, and 21 inches below the top of the tubesheet, the last being at the bottom of the tubesheet.

- At roughly the neutral surface, about 10.5 inches, the contact pressure during SLB is uniformly greater than that during normal operation by about 519 psi (ranging from 499 to 608 psi).
- At a depth of 12.6 inches the contact pressure increase ranges from a maximum of 738 psi near the center of the tubesheet to 531 psi at a radius of 51 inches, see Figure 8.
- At 16.9 inches below the top of the tubesheet and 4.13 inches above the bottom of the tubesheet the contact pressure increases by a maximum of 1231 psi to a minimum of 312 psi at a radius of about 56 inches, Figure 9.
- Near the bottom of the tubesheet, Figure 10, the contact pressure increases by almost 1762 psi near the center of the tubesheet, exhibits no change at a radius of about 56 inches, and diminishes by 680 psi at the extreme periphery, a little more than 60 inches from the center.

A similar comparison is illustrated on Figure 11 at a depth of 8.25 inches from the top of the tubesheet, roughly equal to the originally derived H* depth for the worst location in the tubesheet as determined using SLB conditions. Here the contact pressure increases by 254 and 611 psi at radii of 7.9 and 57 inches respectively, with an average increase of 380 psi. At a depth of about 6 inches from the top of the tubesheet, Figure 12, the contact pressure decreases by about 2 psi near the center of the tubesheet (radius of 7 inches), and increases by a maximum of 596 psi at a radius of 57 inches.

The density of the number of tubes populating the tubesheet increases with the square of the radius. At the H* depth there are far more tubes for which the contact pressure is unchanged or increases at that elevation than there are tubes for which the contact pressure decreases, i.e., greater than 96% of the tubes are at a radius greater than 12 inches from the center of the tubesheet. The leak rate from any indication is determined by the total or integrated resistance of the crevice from the elevation of the indication to the top of the tubesheet, ignoring the resistance from the crack itself. Thus, it would not be sufficient to simply use the depth of 8.25 inches and suppose that the leak rate would be relatively unchanged even if the pressure potential difference were the same.

However, the fact that the contact pressure generally increases below that elevation indicates that the leak rate would be relatively unaffected for indications deeper into the tubesheet. The information on Figure 12 shows that the contact pressure is about the same or greater starting at a depth of 6 inches below the top of the tubesheet, thus, there is an increase in contact pressure for at least 2 inches above the original maximum H* depth. For example, it is assumed that the leak rate would not increase meaningfully from any indications below the mid-plane of the tubesheet. A comparison of the curves on Figure 12 relative to those on Figure 7 indicates that the contact pressure generally increases for a length of at least 4.5 inches upward from the mid-plane for all the tubes in the SG. For radial locations greater than about 10 inches from the center of the tubesheet the length for which the contact pressure increases would be greater than 4.5 inches.

The trend is consistent, at radii where the contact pressure decreases or the increase is not as great near the bottom of the tubesheet, the increase at higher elevations would be expected to compensate. For example, the contact pressures on Figure 10 at the bottom of the tubesheet show a decrease beyond a radius of 56 inches, however, the increase at 8.4 inches above the bottom, Figure 8, is significant. For the outboard tubes the increase in contact pressure extends all the way to the top of the tubesheet. A comparison of the curves at the various elevations leads to the conclusion that for a length of about 11 inches upward from the elevation of 4.26 inches above the bottom of the tubesheet there is always an increase in the contact pressure in going from NOp to SLB conditions. Hence, it is reasonable to omit any consideration of inspection of bulges or other artifacts below a depth of 17 inches from the top of the tubesheet. Applying a very conservative inspection sampling length of 17 inches downward from the top of the tubesheet during the Seabrook fall 2006 and later 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.

6.2 Leakage Analysis from H* Calculations for Comparison

The evaluation of the accident (SLB) leakage for both axial and circumferential cracking in the tube end welds is naturally based on the information presented in WCAP-16053, Reference 5. The leakage analysis uses methods that were developed by Westinghouse to prepare the technical bases for justifying limited RPC inspection depths into the tubesheet expansion region, e.g., Reference 5. The discussion of these methods is included in this report for use at the discretion of Seabrook since examination of the welds is not a recommended action resulting from this report. It is included herein

to provide the potential for dealing with some unexpected eventuality that would lead to a specific examination of the welds.

For axial cracks, a crack confined to the TEW will intersect the TS crevice at only a single point unless the crack extends into the tack expansion zone of the tube above the weld. The intersection of a circumferential crack with the expansion zone crevice would be expected to result in a configuration similar to that of a circumferential crack in the parent tube, bounding both conditions. This is precisely the configuration that was evaluated for both tube retention and potential leak rate in the Reference 5 analyses. The evaluations in that case utilized empirical data developed to quantify the potential leak rate from circumferential cracks located at higher elevations within the tubesheet of Model F SGs. The loading conditions that apply under accident conditions were considered in the leak rate analyses of Reference 5. For example, differential pressure loading on the tubesheet during a SLB event causes tubesheet bowing and affects the tightness of the joint at the TEW. The analyses also considered the potential leak rate from tubes for which the weld was absent. The conclusion from the analyses was that 600 tubes without a tube-to-tubesheet weld and located in the most severe region of the tubesheet would be expected to leak at a total rate of less than 0.141 gpm or about 41% of the site allowable during postulated faulted conditions.

The application of the Reference 5 approach to predicting leak rate has been demonstrated to be conservative to, and obviated by, the application of the bellwether principle and the selection of an inspection depth of 17 inches below the top of the tubesheet. The discussion was included in this report for comparison purposes only and is not planned for application with regard to leak rate prediction calculations described in Reference 5 for the Seabrook fall 2006 and later SG inspection outages.

7.0 Recommendations for Dispositioning Tube Cracks in the Tube-to-Tubesheet Joint

Although the information contained in this report supports using the methodology provided in Reference 5 for indications found within H* for condition monitoring and for assessing the bounding leak rate from non-detected indications in the uninspected range below the H*, its use is not recommended for the fall 2006 (OR11) outage, or subsequent outages, at Seabrook for indications above the 17 inch inspection depth. The evaluations also provide a technical basis for bounding the potential leak rate from non-detected indications in the tube region below 17 inches from the top of the tubesheet as no more that twice the leak rate during normal operation. This applies equally to any postulated indications in the tack expansion region and in the tube-to-tubesheet welds. If cracks are found within the specified inspection depth, it is recommended that the inspection be expanded to include 100% of the tubes in the affected SG using that same specified inspection depth, e.g., 17 inches, as discussed in item 4 of Section 9.0. If the cracking is identified at an existing bulge or over expansion location, the scope expansion can be limited to the population of identified bulges and over expansions within the inspection region. As noted in the introduction to this report, the reporting of crack-like indications in the tube-to-tubesheet welds would be expected to occur inadvertently since no structural or leak rate related technical reason exists for a specific examination to take place.

8.0 Conclusions

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 Appendix A, i.e., Reference 5. 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) Contact forces during postulated LOCA events are sufficient to resist axial motion of the tube. Also, if the tube end welds are not circumferentially cracked, the resistance of the tube-to-tubesheet hydraulic joint is not necessary to resist push-out. Moreover, the geometry of any postulated circumferential cracking of the weld would result in a configuration that would resist pushout in the event of a loss of coolant accident. In other words, the crack flanks would not form the cylindrical surface necessary such that there would be no resistance to expulsion of the tube in the downward direction.

3) The leak rate for indications below the neutral plane of the tubesheet is expected to be bounded <u>on average</u> by twice the leak rate that is present during normal operation of the plant.

4) The accident leak rate for indications below a depth of about 17 inches from the top of the tubesheet would be bounded by twice the leak rate that is present during normal operation of the plant regardless of tube location in the bundle. This is 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.

9.0 Recommended Inspection Plans

The recommendations with regard to the inspection of the welds at Seabrook are based on the following:

1) Examination of the tubes below the H* elevations as described in Reference 5 could be omitted based on structural considerations alone if a license amendment were obtained to that effect.

2) Similar considerations lead to the conclusion that the leak rate during postulated faulted events would be bounded by twice the leak rate during normal operation and the examination of the tube below the specified inspection depth of 17 inches (which includes the tack expansions and the welds) can be omitted from consideration.

3) The prior conclusions rely on the inherent strength and leak rate resistance of the hydraulically expanded tube-to-tubesheet joint, a feature which was not considered or permitted to be considered for the original design of the SG. Thus, omission of the inspection of the weld constitutes a reassignment of the pressure boundary to the tube-to-tubesheet interface. Similar considerations for tube indications require NRC staff approval of a license amendment.

Westinghouse endorses the following SG tube inspection plan with regard to the tubesheet region in the four Seabrook SGs during, at least, the next inspection, OR11, when all four SGs are scheduled

to be inspected. Specific sampling plans for future outage will depend on the scope of the inspection planned by Seabrook as identified in the applicable Degradation Assessments.

1. Performance of a minimum of a 20% inspection of the hot leg side tubes using RPC technology from 3 inches above the top of the tubesheet to 3 inches below the top of the tubesheet. Expand to 100% of the affected SG and 20% of the unaffected SGs in this region only if cracking is found that is not associated with a bulge or overexpansion as described below. This scope is no different than usually planned for the top-of-tubesheet inspection of the tubes.
2. Perform an inspection of hot leg side tubes using RPC technology to a maximum depth of 17 inches below the top of the tubesheet based on obtaining a 20% sample of bulges and over expansions. The size of the sample is to be developed as a fraction of the parent tube population of bulges ≥ 18 Volts and over expansions ≥ 1.5 mils on the diameter as obtained from a review of the data from a previous operating cycle for Seabrook for a minimum length of 17 inches below the top of the tubesheet. The inspection of a single tube can simultaneously contribute to meeting the scope of both inspection items 1 and 2.
3. If cracking is found in the sample population of bulges or over expansions, the inspection scope should be increased to 100% of the population of bulge and overexpansion locations for the region of the top of the tubesheet to a depth of 17 inches in the affected SG and a 20% sample of each of the unaffected SGs.
4. If cracking is reported at one or more tube locations not designated as either a top of the tubesheet expansion transition, a bulge or an over expansion, an engineering evaluation can be performed aimed at determining the cause for the signal, e.g., some other tubesheet anomaly, in order to identify a critical area for the expansion of the inspection. This inspection will be limited to the original specified depth of 17 inches.

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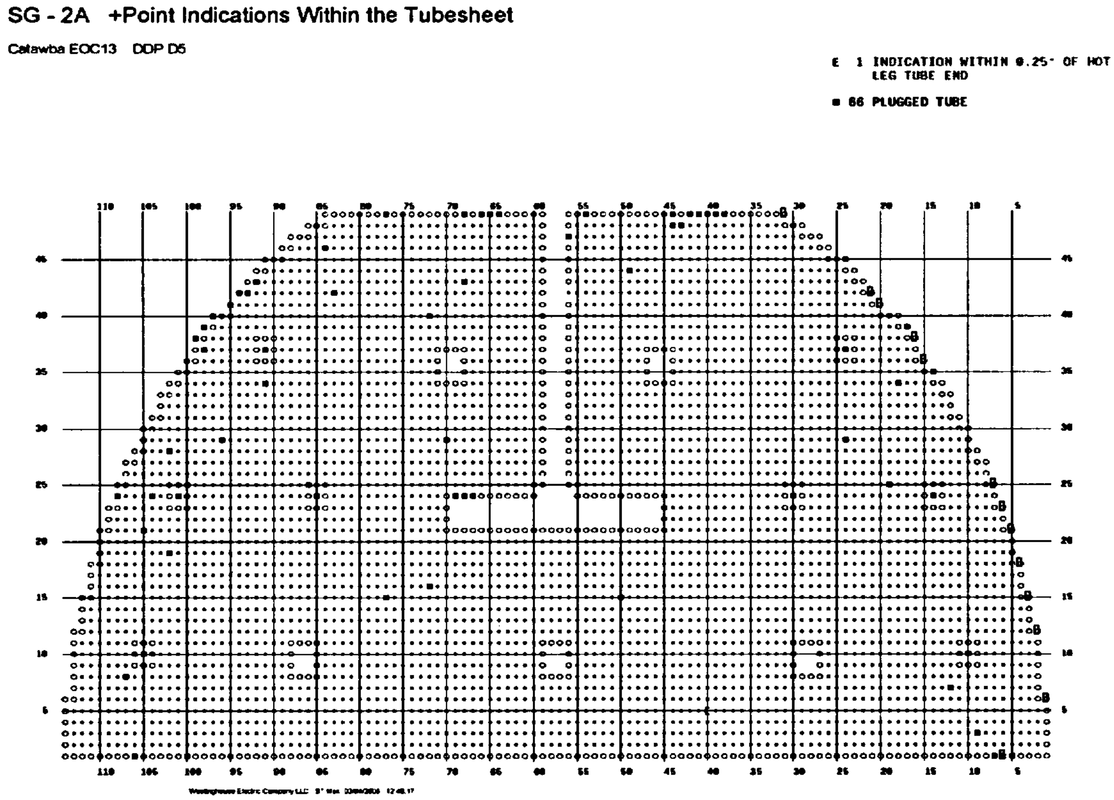

Figure 1: Distribution of Indications in SG A at Catawba 2

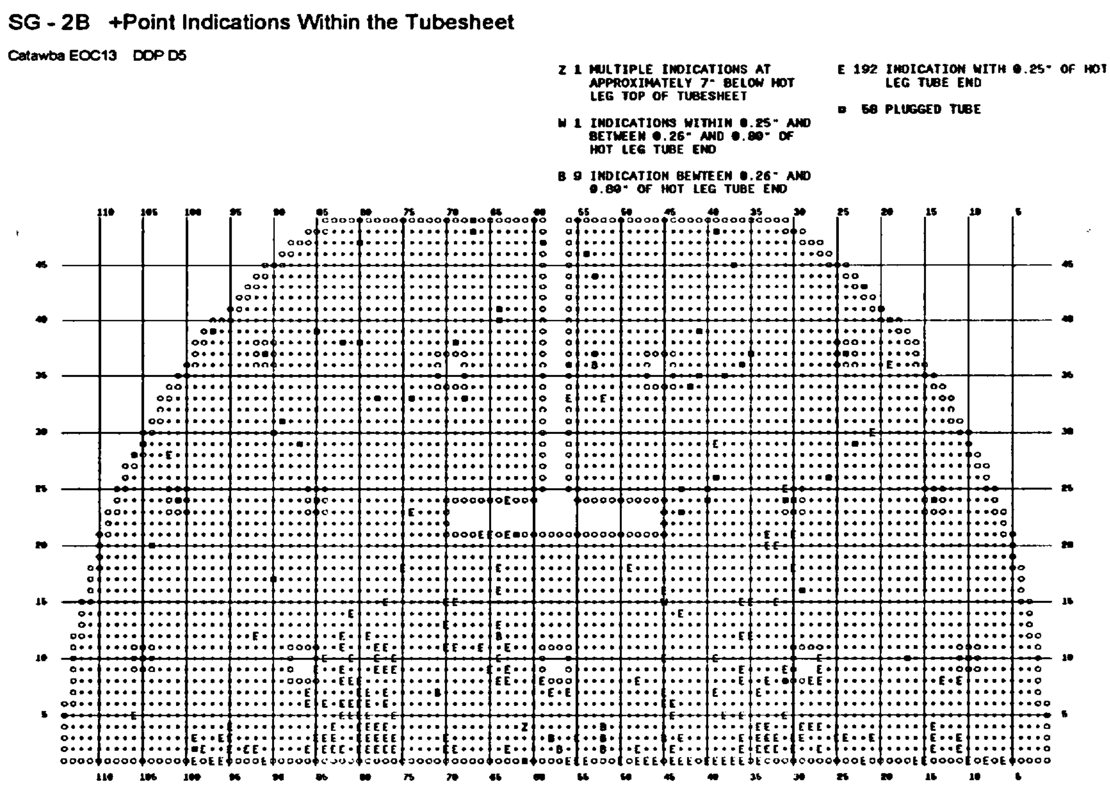

Figure 2: Distribution of Indications in SG B at Catawba 2

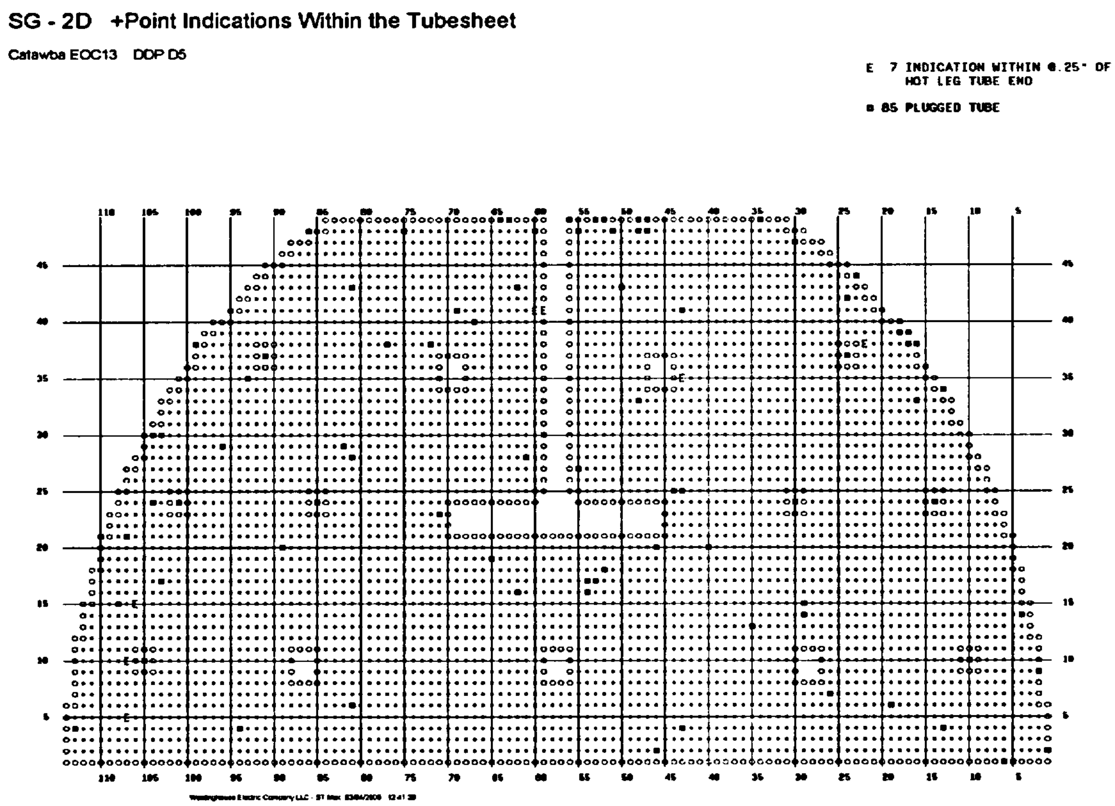

Figure 3: Distribution of Indications in SG D at Catawba 2

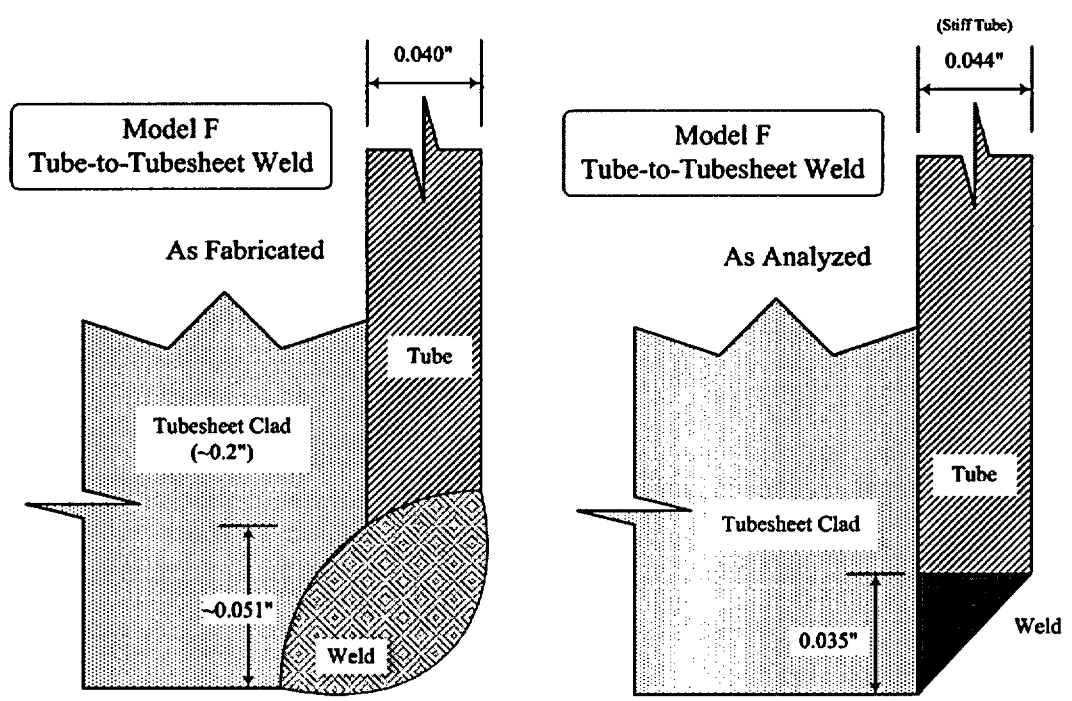

Figure 4: As-Fabricated & Analyzed Tube-to-Tubesheet Welds

a,c,e

Figure 5: Definition of H* Zones from Reference 5

a,c,e

Figure 6: Flow Resistance Curve per Reference 5

a,c,e

Figure 7: Change in contact pressure at 10.5 inches below the TTS

a,c,e

Figure 8: Change in contact pressure at 12.6 inches below the TTS

a,c,e

Figure 9: Change in contact pressure at 16.9 inches below the TTS

a,c,e

Figure 10: Change in contact pressure near the bottom of the tubesheet

a,c,e

Figure 11: Change in contact pressure at 8.25 inches below the TTS

a,c,e

Figure 12: Change in contact pressure at 6.0 inches below the TTS

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Appendix A — Structural Analysis of the Tube-to-Tubesheet Contact Pressure

A. Structural Analysis of the Tube-to-Tubesheet Interface Joint

The following information regarding the structural analysis of the interactions of the tube and tube tubesheet during normal and faulted conditions was extracted in its entirety from Reference A-1 for inclusion in this report to permit a "stand-alone" review of the submitted information.

An evaluation was performed to determine the contact pressures between the tubes and the tubesheet in the Seabrook steam generators as part of the H* analysis. The evaluation utilized [

]a,c,e, were determined.

The same contact pressure results were used [

]a,c,e were also included.

A.1 Evaluation of Tubesheet Deflection Effects for Tube-to-Tubesheet Contact Pressure

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

]a,c,e loads in the tube.

A.1.1 Material Properties and Tubesheet Equivalent Properties

The material of construction for the tubing in the steam generators is a nickel base alloy, Alloy 600; the tubes are in the thermally treated (TT) condition. Summaries of the applicable mechanical and thermal properties for the tube material are provided in Table A.1. The tubesheet material is

SA-508, Class 2a, and its properties are in Table A.2. The shell material is SA-533 Grade A Class 2, and its properties are in Table A.3. Finally, the channel head material is SA-216 Grade WCC, and its properties are in Table A.4. The material properties are from Reference A-7.

The perforated tubesheet in the Model F channel head complex is treated [

]^{a,c,e} in the perforated region of the tubesheet for the finite element model. The material properties of the tubes are not utilized in the finite element model, but are listed in Table A.1 for use in the calculations of the tube/tubesheet contact pressures.

A.1.2 Tubesheet Rotation Effects

Loads are imposed on the tube as a result of tubesheet rotations under pressure and temperature conditions. [

]^{a,c,e}.

Previous calculations performed [

]^{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:

$$\left[\qquad \right]^{a,c,e}$$

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:

$$\left[\qquad \right]^{a,c,e}$$

UR is available directly from the finite element results and 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

This hole expansion (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 A-10), 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 is given by:

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

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 = 0.33943 in.
c = Outside radius of tube = 0.382 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. The expansion of the hole ID produced by internal 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 to its potential use elsewhere in this report.)

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

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

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

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:

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

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

The resulting equation is:

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[ ]

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

The OD of the tubesheet cylinder is equal to that of the cylindrical (simulate) collars (1.632 inches) designed to provide the same radial stiffness as the tubesheet, which was determined from a finite element analysis of a section of the tubesheet (Reference A-10).

The tube inside and outside radii within the tubesheet are obtained by assuming a nominal diameter for the hole in the tubesheet (0.7035 inch, selected as 0.703 inch in this case) and wall thinning in the tube equal to the average of that measured during hydraulic expansion tests. That thickness is 0.0396 inch for the tube. The following table lists the values used in the equations above, with the material properties evaluated at 600°F. (Note that the properties in the following sections are evaluated at the primary fluid temperature).

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

A.1.3 Seabrook Contact Pressures

A.1.3.1 Normal Operating Conditions

The loadings considered in the analysis are based on an umbrella set of conditions as defined in References A-11 through A-15. The current operating parameters from Reference A-15 were used. The temperatures and pressures for normal operating conditions at Seabrook are therefore:

Loading	Case 1 (1)	Case 2 (2)
Primary Pressure	2235 psig	2235 psig
Secondary Pressure	782 psig	947 psig
Primary Fluid Temperature (T_{hot})	604.3°F	621.4°F
Secondary Fluid Temperature	517.8°F	540.0°F
(1) T_{hot} reduced.		
(2) Maximum T_{hot} maintained at 620.0°F.		

The primary pressure [

]^{a,c,e}.

A.1.3.2 Faulted Conditions

Of the faulted conditions, Feedline Break (FLB) and Steamline Break (SLB) are the most limiting. FLB has a higher ΔP across the tubesheet, while the lower temperature of SLB results in less thermal tightening. Both cases are considered in this section.

Previous analyses have shown that FLB and SLB are the limiting faulted conditions, 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 FLB and SLB (References A-12 and A-13). Therefore LOCA was not considered in this analysis.

A.1.3.2.1 Feedline Break

The temperatures and pressures for Feedline Break at Seabrook are bracketed by the following two cases:

Loading	T_{min} (1)	T_{max} (2)
Primary Pressure	2650 psig	2650 psig
Secondary Pressure	0 psig	0 psig
Primary Fluid Temperature (T_{hot})	580.3°F	597.4°F
Secondary Fluid Temperature	517.8°F	540.0°F

(1) T_{hot} reduced.
(2) Maximum T_{hot} maintained at 620°F.

The Feedline Break condition [

]^{a,c,e}.

A.1.3.2.2 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 pertinent parameters are listed below. The combination of parameters yielding the most limiting results is used.

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

For this set of primary and secondary side pressures and temperatures, the equations derived in Section A.1.2 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.

A.1.4 Summary of FEA Results for Tube-to-Tubesheet Contact Pressures

For Seabrook, the contact pressures between the tube and tubesheet for various plant conditions are listed in Table A.5 and plotted versus radius in Figure A.2 through Figure A.6.

A.2 Determination of Required Engagement Length of the Tube in the Tubesheet

The H* partial-length RPC justification relies on knowledge of the tube-to-tubesheet interfacial mechanical interference fit contact pressure at all elevations in the in the tube joint, especially in the upper half of the tube joint. The contact pressure is used for both anchorage of the tube in the tubesheet in the H* evaluation and for determining the leakage effects for H*.

For the tube anchorage effect, it is necessary to demonstrate that the tube resistance to pullout exceeds the pullout load by the required margin. For the leakage resistance effect, the leakage flow loss coefficient is determined as a function of several variables, including the contact pressure. The contact pressure was determined semi-empirically. It was determined by test for the as-installed (factory) condition and then analytically projected to the pertinent plant conditions. The test involved pullout testing.

Testing has been performed on full depth hydraulic expansions of 11/16 inch diameter tubes into a tubesheet-simulating test collar. Following expansion of the tubes, the sample was subjected to a high temperature soak to simulate the stress relief of the channel head-to-tubesheet weld. The lowest force required to cause first slip (breakaway) of non-welded Alloy 600 tubes which were conservatively expanded at [

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The end cap loads for Normal and Faulted conditions are:

Normal (maximum):	$\pi * (2235-782) * (0.703)^2 / 4 = 563.98$ lbs.
Faulted (FLB):	$\pi * 2835 * (0.703)^2 / 4 = 1028.60$ lbs.
Faulted (SLB):	$\pi * 2560 * (0.703)^2 / 4 = 993.67$ lbs.

Thus, based on the guidelines of RG 1.121, the critical endcap load is 1692 lbs., which is three times the normal load and is greater than 1.43 times the accident operation loads of 1471 lbs. (FLB) and 1421 lbs. (SLB).

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 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* (Reference A-14).

Two series of test data are available for the determination of the residual strength of the joint. Data were obtained from a factory shop test program which was performed to investigate the various manufacturing steps associated with the tube-to-tubesheet joint relative to the use of Alloy 600 tube material. A second series of tests was performed to investigate the integrity of the tube-to-tubesheet joints as a consequence of extensive tube end foreign object damage being experienced at a plant with Model F SGs.

The data from both series of pullout tests are listed in Table A.11. The first [

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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 = 118 lb/inch,
P = Contact pressure acting over the incremental length segment dh, and,
μ = 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 A.6 through Table A.10, so that between elevations L_1 and L_2,

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

or,

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so that,

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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 A.6 through Table A.10. The above equation is also used to find the minimum contact lengths needed to meet the pullout force

requirements (Reference A-14). This length is 8.50 inches for the limiting 3 times normal operating pressure performance criterion which corresponds to a pullout force of 1680 lbs in the Hot Leg (Reduced T_{hot}).

The top part of Table A.8 lists the contact pressures through the thickness at each of the radial sections for Faulted (SLB) condition. The last row H(0) of this table lists the maximum tubesheet elevation at which the contact pressure is greater than or equal to zero. The above equation is used to accumulate the force resisting pull out from the top of the tubesheet to each of the elevations listed in the lower part of Table A.8. The above equation is also used to find the minimum contact lengths needed to meet the pull out force requirements. This length is 8.16 inches for Faulted (SLB) condition. The minimum contact length needed to meet the pullout force requirement for Faulted (FLB) condition is less as is shown in Table A.9 and Table A.10.

Therefore, the bounding condition for the determination of the H* length is the NOp performance criterion. The minimum contact length for the normal operating condition is 8.46 inches in Zone D.

A.3 References

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A-12. CN-SM-98-102 (Proprietary), Rev. 2, "Tube/Tubesheet Contact Pressures for Yonggwang 2," Westinghouse Electric Company LLC, Pittsburgh, PA, November 1998.

A-13. CN-SGDA-02-152 (Proprietary), Rev. 1, "Evaluation of the Tube/Tubesheet Contact Pressures for Callaway Model F Steam Generators," Westinghouse Electric Company LLC, Pittsburgh, PA, 2002.

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A-15. CN-SGDA-03-99 (Proprietary), "Evaluation of the Tube/Tubesheet Contact Pressures for Seabrook, Seabrook, and Vogtle 1 and 2 Model F Steam Generators," Westinghouse Electric Company LLC, Pittsburgh, PA, September 2003.

A-16. CN-SGDA-03-121 (Proprietary), "H* Ligament Tearing for Models F and D5 Steam Generators," Westinghouse Electric Company LLC, Pittsburgh, PA, October 2003.

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

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

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

Table A.4: Summary of Material Properties SA-216 Grade WCC Channelhead Material							
	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 A.5: Summary of Tube/Tubesheet Maximum & Minimum Contact Pressures & H* for Seabrook Steam Generators

a,c,e

Table A.6: Cumulative Forces Resisting Pull Out from the TTS
Seabrook – Hot Leg Normal Conditions – Reduced T_{hot}, P_{sec} = 782 psig

a,c,e

Table A.7: Cumulative Forces Resisting Pull Out from the TTS
Seabrook – Hot Leg Normal Conditions – T_{hot} = 621.4°F, P_{sec} = 947 psig

a,c,e

Table A.8: Cumulative Forces Resisting Pull Out from the TTS
Seabrook – Faulted (SLB) Conditions, P_{sec} = 0 psig

a,c,e

Table A.9: Cumulative Forces Resisting Pull Out from the TTS
Seabrook – Faulted (FLB) Conditions, Reduced T_{hot}

a,c,e

Table A.10: Cumulative Forces Resisting Pull Out from the TTS
Seabrook – Faulted (FLB) Conditions, T_{hot} = 620°F

a,c,e

Table A.11: Large Displacement, ~0.2 to 0.3 in. Pullout Test Data
(Assume μ of 0.3 for contact pressure determination.)

a,c,e

Table A.12: Pullout Tests Initial Slip Data

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a,c,e

Figure A.1: Finite Element Model of Model D5-3 Tubesheet Region

a,c,e

Figure A.2: Contact Pressures for NOp at Seabrook, Reduced T_{hot}, P_{sec} = 782 psig

a,c,e

Figure A.3: Contact Pressures for NOp at Seabrook, T_{hot} = 621.4°F, P_{sec}= 947 psig

a,c,e

Figure A.4: Contact Pressures for SLB Faulted Condition at Seabrook

a,c,e

Figure A.5: Contact Pressures for FLB Faulted Condition at Seabrook (Reduced T_{hot})

a,c,e

Figure A.6: Contact Pressures for FLB Faulted Condition at Seabrook (T_{hot} = 621.4°F)