ATTACHMENT 2

NON-PROPRIETARY WESTINGHOUSE ELECTRIC COMPANY, LLC LTR-CDME-06-17-NP, "STEAM GENERATOR TUBE ALTERNATE REPAIR CRITERIA FOR THE PORTION OF THE TUBE WITHIN THE TUBESHEET AT CATAWBA 2 IN SUPPORT OF CYCLE 15 OPERATION"

LTR-CDME-06-17-NP

Steam Generator Tube Alternate Repair Criteria for the Portion of the Tube Within the Tubesheet at Catawba 2 in Support of Cycle 15 Operation

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Abstract

Nondestructive examination indications of primary water stress corrosion cracking were found in the Westinghouse Model D5 Alloy 600 thermally treated steam generator tubes at the Catawba 2 nuclear power plant in the fall of 2004. Most of the indications were located in the tube-to-tubesheet welds with a few of the indications being reported as extending into the parent tube. In addition, a small number of tubes were reported with indications about 3/4 inch above the bottom of the tube within a region referred to as the tack-expansion, and multiple indications were reported in one tube at internal bulge locations in the upper third of the tubesheet. The tube end weld indications were dominantly axial in orientation and almost all of the indications were concentrated in one steam generator. Circumferential cracks were also reported at internal bulge locations in two of the Alloy 600 thermally treated steam generator tubes at the Vogtle 1 plant site in the spring of 2005. Based on interpretations of requirements published by the NRC staff in GL 2004-01 and IN 2005-9, Duke Power requested that an alternate repair criterion be developed for the tubesheet regions of the steam generator tubes at the Catawba 2 power plant. 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 2.3 to 8.6 inches (depending on the tube leg), designated as H*, and, 2) that the accident condition leak rate integrity can be bounded by twice the normal operation leak rate from degradation below 17 inches from the top of the 21 inch thick tubesheet, including degradation of the tube end welds. These results follow from analyses demonstrating that the tube-to-tubesheet hydraulic joints make it extremely unlikely that any operating or faulted condition loads are transmitted below the H* elevation, and the contact pressure dependent leak rate resistance increases below the neutral plane within 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 a steam line break to normal operation comparative leak rate evaluation. Application of the structural analysis and leak rate evaluation results to eliminate inspection and/or repair of tube indications in the region of the tube below 17 inches from the top of the tubesheet 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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Steam Generator Tube Alternate Repair Criteria for the Portion of the Tube Within the Tubesheet at Catawba 2

1.0 Introduction

Indications of cracking were reported 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 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 tube material. The spatial distribution of indications by row and column number is shown on Figure 1-1 for SG A, Figure 1-2 for SG B, and Figure 1-3 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. 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 4). The Vogtle SGs are of the Westinghouse Model F design with slightly smaller, diameter and thickness, A600TT tubes. It has been concluded from these observations that 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.

> Note: No indications were found during the planned inspections of the Braidwood 2 SG tubes in April 2005, a somewhat similar inspection of the tubes in two Model F SGs at Wolf Creek in April 2005, or an inspection of the Model D5 tubes at Comanche Peak 2 in the spring of 2005. Nor during similar inspections of the Model D5 tubes at Byron 2 and Model F tubes at Vogtle 1 in the fall of 2005.

The SGs at the 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 different process for effecting the tack expansions was adopted prior to the time of the fabrication of the Braidwood 2 SGs.

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 5, and regulatory issues, Reference 6, and,

b) provide analysis support for technical arguments to ignore tube degradation within the tubesheet of any extent below a specified depth or depths as a function of tube location, i.e., the depths specified in Section 6.0 of this report.

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

1) the structural integrity of the primary-to-secondary pressure boundary is unaffected by tube degradation of any magnitude below a tube location-specific depth ranging from about 2.3 and 3.5 to 8.1 and 8.6 inches on the hot and cold legs respectively, criteria which are designated as H*, and,

2) the accident condition leak rate integrity can be meaningfully bounded by twice the normal operation leak rate from degradation below 17 inches from the top of the 21 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 tube-to-tubesheet contact leak rate resistance increases below the 17 inch elevation 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 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 ignore tube degradation in 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.

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 7. Subsequent approvals were also obtained for use at Byron 2 and Vogtle 2 for their fall 2005 inspection campaigns, Reference 7 for example. This report was prepared to justify the specialized probe 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 8. The bases for the performance criteria are the demonstration of both structural and leakage integrity during normal operation and postulated accident conditions. The structural model was based on standard analysis techniques and finite element models as used for the original design of the SGs and documented in

numerous submittals for the application of criteria to deal with tube indications within the tubesheet of other models of Westinghouse designed SGs with tube-to-tubesheet joints fabricated by other techniques, e.g., explosive expansion.

All full depth expanded tube-to-tubesheet joints in Westinghouse-designed SGs have a residual radial preload between the tube and the tubesheet. Early vintage SGs involved hard rolling which resulted in the largest magnitude of the residual interface pressure. Hard rolling was replaced by explosive expansion which resulted in a reduced magnitude of the residual interface pressure. Finally, hydraulic expansion replaced explosive expansion for the installation of SG tubes, resulting in a further reduction in the residual interface pressure. In general, it was found that the leak rate through the joints in hard rolled tubes, if any, is insignificant. Testing demonstrated that the leak rate resistance of explosively expanded tubes was not as great and prediction methods based on empirical data to support theoretical models were developed to deal with the potential for leakage. The same approach was followed to develop a prediction methodology for hydraulically expanded tubes. However, the model has been under review since its inception, with the intent of verifying its accuracy because it involved analytically combining the results from independent tests of leak rate through cracks with the leak rate through the tube-to-tubesheet crevice. The leak rate model associated with the initial development of H* to meet structural performance criteria is such a model; technical acceptance could be time consuming since it has not been previously reviewed by the NRC staff. An alternative approach was developed for application at Catawba 2 from engineering expectations of the relative leak rate between normal operation and postulated accident conditions based on a first principles engineering approach.

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. Section 5.0 addresses the structural analysis of the tube-to-tubesheet joint. Section 6.0 discusses the leak rate analysis, and finally, the conclusions from the structural and leak rate evaluations are contained in Section 7.0.

SG - 2A +Point Indications Within the Tubesheet

Catawba EOC13 DDP D5

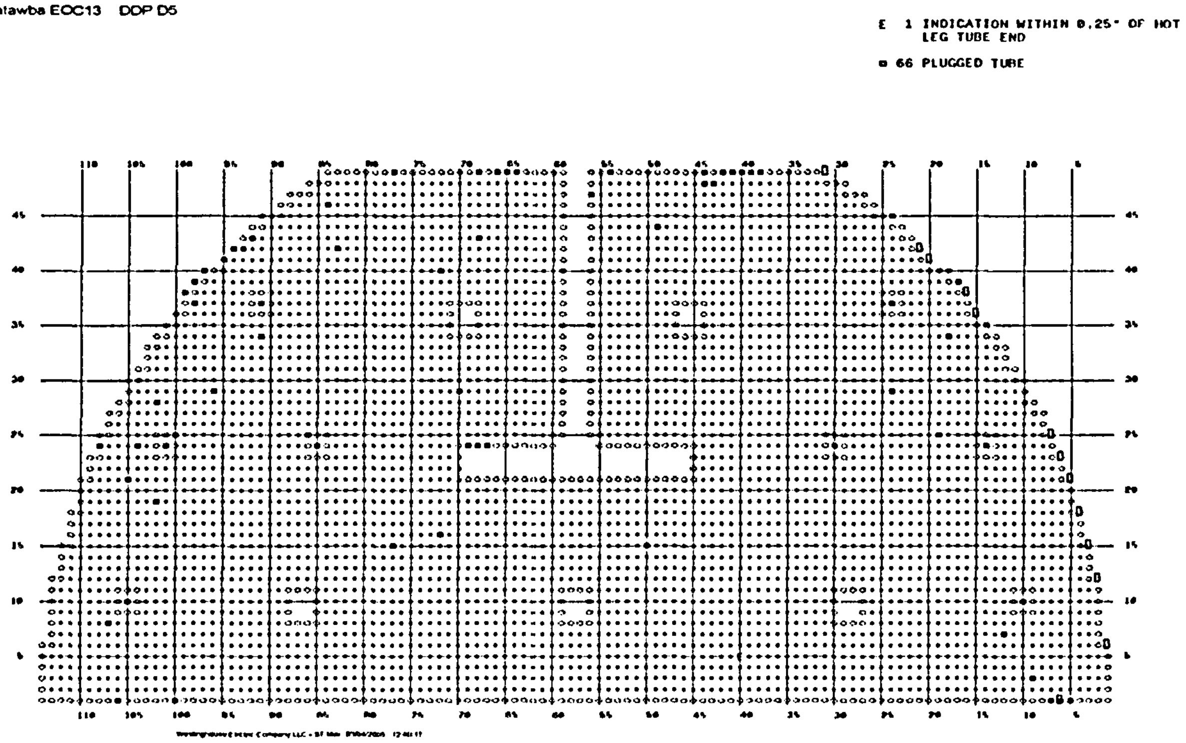

Figure 1-1. Distribution of Indications in SG A at Catawba 2

SG - 2B +Point Indications Within the Tubesheet

Catawba EOC13 DDP D5

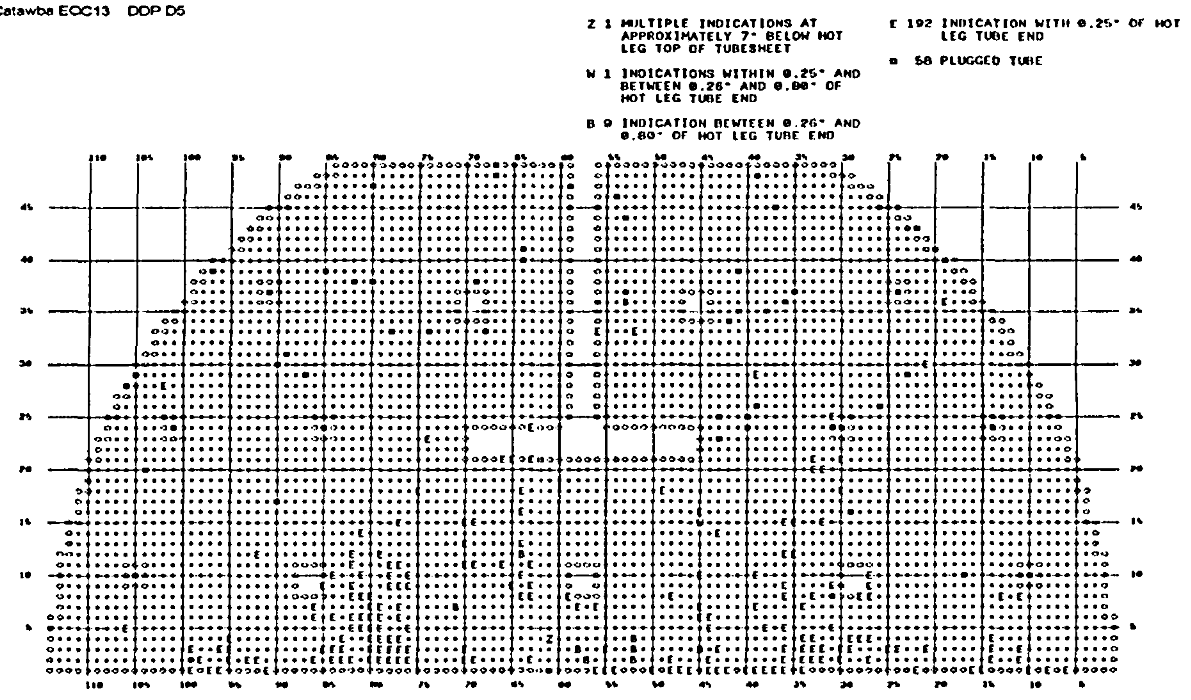

Figure 1-2. Distribution of Indications in SG B at Catawba 2

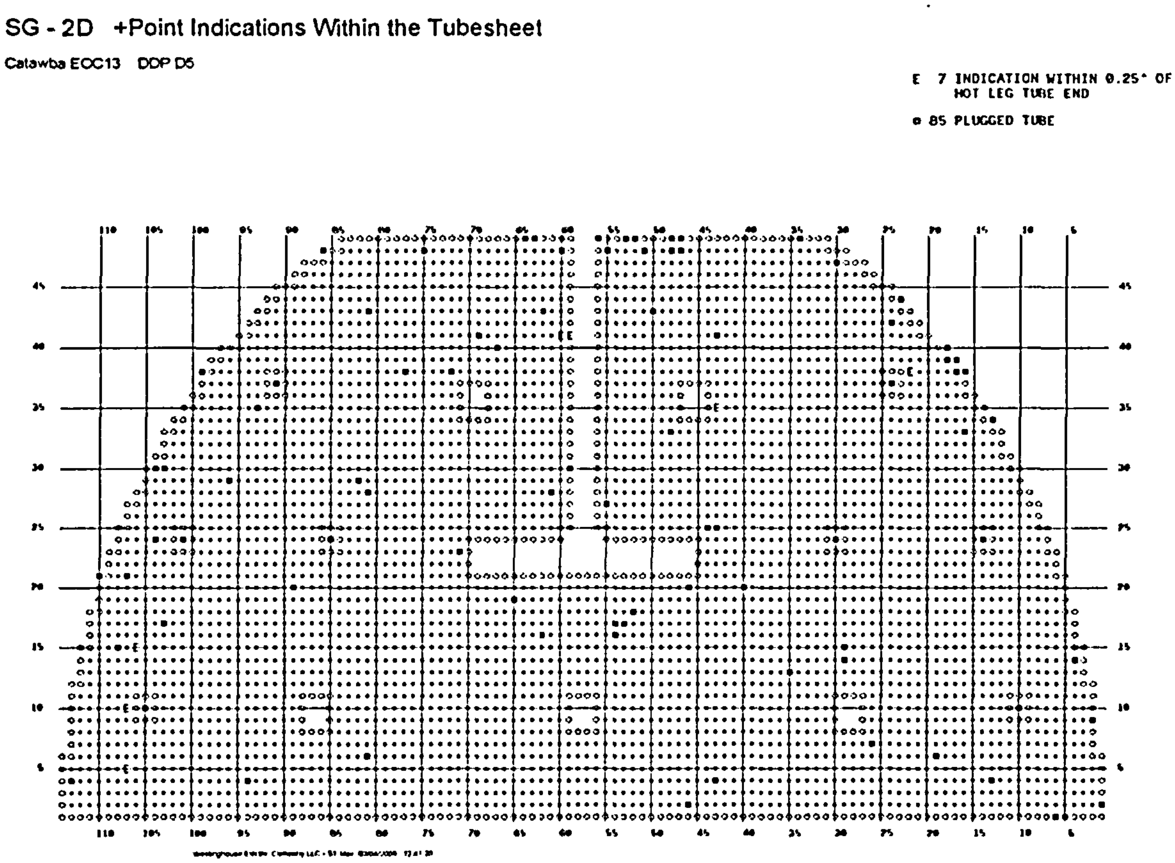

Figure 1-3. Distribution of Indications in SG D at Catawba 2

2.0 Summary Discussion

Evaluations were performed to assess the need for addressing degradation in the region of the SG tubes below a specified distance within the tubesheet at the Catawba 2 power plant. The conclusions from the evaluation are that a redefinition of the pressure boundary can be effected while still assuring that the structural and leak rate performance criteria would be met during both normal operation and limiting postulated accident conditions.

The result of effecting such a redefinition would be the elimination of repairing tubes for degradation below a depth on the order of 17[1] inches from the top of the tubesheet, including 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 address 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. It is concluded that there is no need to inspect 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 can also be concluded that for some of the tubes, depending on radial location in the tubesheet, there is no need to address the region of the tube below the shifted neutral plane of the tubesheet with regard to contact pressure,[2] roughly 8.3 inches below the top. The results from the evaluations performed as described herein demonstrate that the inspection of the tube within about 10 inches of the tube-to-tubesheet weld and of the weld is not necessary for structural adequacy of the SG during normal operation or during postulated faulted conditions, nor for the complying with leak rate limits during postulated faulted events. This conclusion applies to both the hot and cold legs of the SG tubes.

In summary:

- The structural integrity requirements of NEI 97-06, Reference 8, are met by sound tube engagement lengths ranging from about 3.5 to 8.6 inches from the top of the tubesheet, regardless of hot or cold leg considerations, 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 (see Table A-13).

- NEI 97-06, Reference 8, 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 9.

- 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 Code, Winter 1972 Addenda with selected sections of the Winter 1974 Addenda, References 5, 10 and 14. The analyses are documented in Reference 12 for the Catawba 2 SGs. The typical as-fabricated and the as-analyzed weld configurations are illustrated on

1 The value of 17 inches is based on confirming leak rate expectations and could be considerably less.

2 The neutral plane of the tubesheet is shifted away from the center because of the tensile stress in the tubesheet and the internal pressure in the tube.

Figure 2-1. The "stiff tube" representation was an analysis feature to maximize the stress in the weld.

- Section XI of the ASME Code, 1971 through 2004, References 13 and 14, 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 D5 SGs are not leak-tight without the tube end weld and considerations were also made with regard to the potential for primary-to-secondary leakage during postulated faulted conditions. However, the leak rate during postulated accident conditions would be expected to be less than that during normal operation for indications near the bottom of the tubesheet (including indications in the tube end welds) based on the observation that while the driving pressure increases by about a factor of almost two, the flow resistance increases because the tube-to-tubesheet contact pressure also increases. Depending on the depth within the tubesheet, the relative increase in resistance could easily be larger than that of the pressure potential. Therefore, the leak rate under normal operating conditions could exceed its allowed value before the accident condition leak rate would be expected to exceed its allowed value. This approach is termed an application of the "bellwether principle." While such a decrease in the leak rate 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.

Based on the information summarized above, no inspection of the tube-to-tubesheet welds, tack roll region or bulges below the distance determined to have the potential for safety significance as specified in Reference 5, i.e., the H* depths, would be necessary to assure compliance with the structural requirements for the SGs. In addition, based on the results of the application of the bellwether principle regarding the potential leakage during postulated accident conditions, the planned alternate repair criterion/inspection length of 17 inches below the top of the tubesheet is conservative and justified.

The selection of the depth of 17 inches obviates the need to consider the location of the tube expansion transition below the top of the tubesheet, usually bounded by a length of 0.3 inches. For structural purposes, the value of 17 inches greatly exceeds the engagement lengths determined from the H* analysis included in Appendix A of this report. The application of the bellwether approach to the leak rate analysis as described in Section 6.0 of this report negates the need to consider specific distances from the top of the tubesheet and it relies only on the relative magnitude of the joint contact pressure in the vicinity of the tube above 17 inches below the top of the tubesheet.

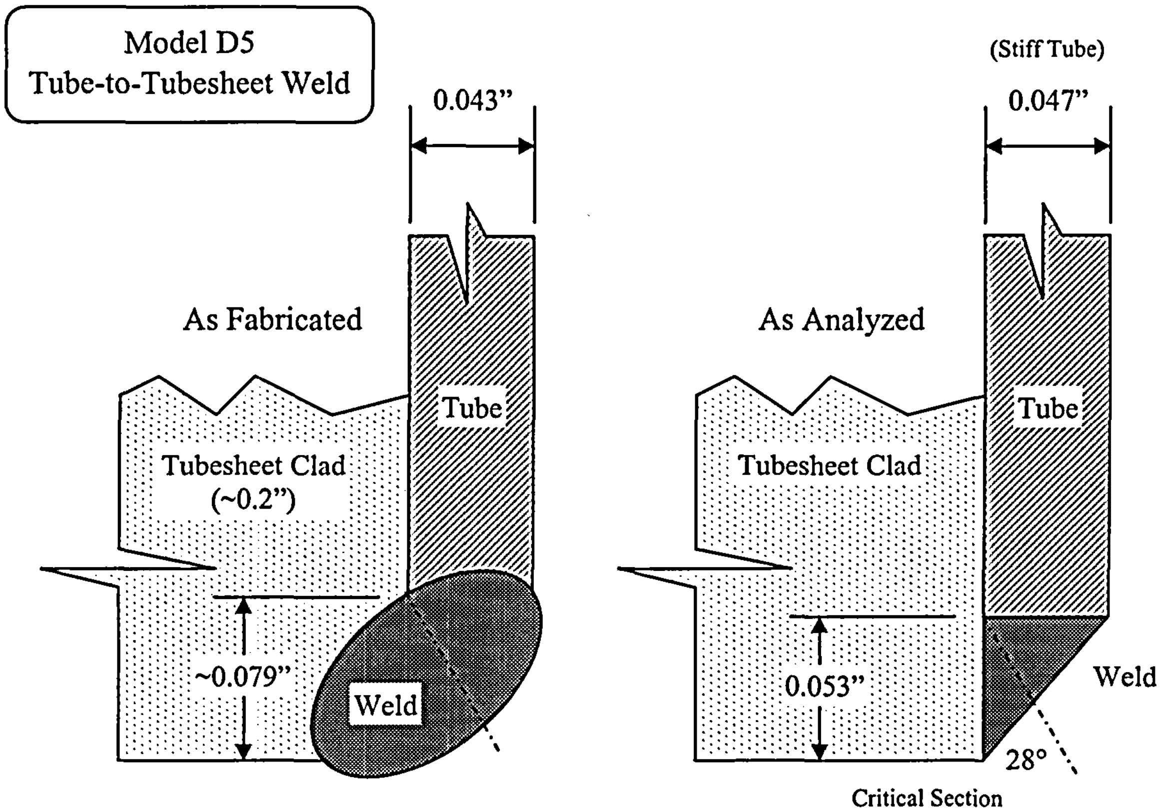

Figure 2-1. As-Fabricated & As-Analyzed Tube-to-Tubesheet Welds

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 at the bottom of the tubesheet referred to as the tack expansion. 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. The rolling process was used during fabrication of the Catawba 2 SGs. The tube-to-tubesheet weld was then performed to create the ASME Code pressure boundary between the tube and the tubesheet.[3]

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 18. 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 16. 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 17. The W* criteria were first applied to operating SGs in 1999 based on a generic evaluation for Model 51 SGs, Reference 18, and the subsequent safety evaluation by the NRC staff, Reference 19. 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 usually shorter than, that needed to meet the leakage integrity requirements.

The post-weld expansion of the tube into the tubesheet in the Catawba 2 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 Catawba 2 SGs, on the structural and leakage integrity of the joint, References 20 and 21. It was concluded in that evaluation that the strength of the tube-to-tubesheet joint is sufficient to prevent pullout in

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

accordance with the requirements of the performance criteria of Reference 8 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 (References 6and 8). Reference 6 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 8):

> *"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 Reference 13 for example. 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 the Tube-to Tubesheet Joint

This section summarizes the structural aspects and analysis of the entire tubesheet joint region, the details of which are provided in Appendix A of this report. 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 Code, Summer 1972 Addenda, with selected sections of the Winter 1974 Addenda, References 5, 10 and 14. The analyses are documented in Reference 12 for the Catawba 2 SGs.

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 Catawba 2 Model D5 SGs. Typical Model D5 hydraulic expansion joints with lengths ranging from 3 to 7 inches, which are comparable to those discussed in what follows for limiting specialized probe examination considerations, 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 3.45 to 8.61 inches (regardless of tube leg) 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 a little 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 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, including postulated loss of coolant accidents (LOCA), and 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 A.2.

An examination of Table A-7 through Table A-11 illustrates that the holding power of the tube-to-tubesheet joint in the vicinity of 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* as listed in Table A-12. Note that the radii reported in these tables were picked to conservatively represent the entire radial zones of consideration as defined on Figure A-1. For example, Zone C has a maximum radius of 34.4 inches. However, 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,

[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.

i.e., those values calculated for a tube at a radius of 4.08 inches. The values are everywhere conservative above the neutral surface of the tubesheet for tubes in Zone C. Likewise for tubes in Zone B under the heading 49.035 inches where the basis for the calculation was a tube at a radius of 34.4 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 2.5 inch range from 8 to 10.5 inches below the top of the tubesheet is about 916 lbf per inch during normal operation. Hence, the performance criterion for 3·ΔP during normal operation is met by the first 2.2 inches of engagement above a depth of 11 inches and at a radius of 59 inches the length of engagement needed is about 1.9 inches. The corresponding values for SLB conditions are 1.70 and 1.40 inches at radii of 4.08 and 58.8 inches respectively. In other words, while a HL value of 8.13 inches was determined for H* from the top of the tubesheet, a length of 2.2 to 2.1 inches would be sufficient at the bottom of an inspection depth of 11 inches, where the latter value corresponds to a radius of 34.4 inches from the center of the tubesheet, the maximum extent of Zone C.

Finally, at a depth of 17 inches, engagement lengths of less than 2 inches are needed to resist the pullout force associated with a differential pressure of 3·ΔP for normal operation and less than 1.5 inches for SLB conditions. The latter value applies to peripheral locations where the joint is actually tighter near the mid-plane than at the bottom. At central locations the length of engagement needed to resist pullout is more on the order of one inch.

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 at a distance 17 inches from the top of the tubesheet. 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 6. It is noted that the methods discussed below support a change to the Catawba 2 Technical Specifications for Cycle 15 operation. 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.

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 near or 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 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 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 were 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, ignoring any reduction in temperature associated with the event. Based on early NRC staff queries regarding the leak rate modeling code associated with calculating the expected leak rate, see Reference 22 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 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

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

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 34 inches from the center of the SG, the contact pressure during normal operation is calculated to be about 2180 to 2225 psi[6], see the last contact pressure entry in the center columns of Table A-7 and Table A-8 respectively, while the contact pressure during a postulated steam line break would be on the order of 3320 psi at the bottom of the tubesheet, Table A-9, and during a postulated feed line break would be on the order of 4240 psi at the bottom of the tubesheet, Table A-10 and Table A-11 are close to the same number.

> 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 4.08 inches. Also, the values tabulated do not include the calculated residual preload from the tube installation, which is not necessary for this comparison.

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 6-1 indicates that the crevice resistance to flow per unit length (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

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

a path that is formed between the points of hard contact between the tube and the tubesheet as a result of the differential thermal expansion and the internal pressure in the tube. There is both mechanical dispersion and molecular diffusion taking place. The net result is that the flow is best described as primary-to-secondary weepage. At its base, the expression used to predict the leak rate from tube cracks through the tube-to-tubesheet crevice is the Darcy expression for flow rate, Q, through porous media, i.e.,

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

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

If the leak rate during normal operation was 0.05 gpm (about 75 gpd), the postulated accident condition leak rate would be on the order of 0.1 gpm if only the change in differential pressure were considered, however, the estimate would be reduced when the increase in contact pressure between the tube and the tubesheet was included during a postulated steam line break event (and further reduced if an attendant potential increase in viscosity were considered). 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-7 through Table A-11 shows that the bellwether principle applies to a significant extent to all indications below the neutral plane of the tubesheet, and may apply to somewhat higher elevations. At the central plane of the tubesheet, the increase in contact pressure shown on Figure 6-5 is more on the order of 250 psi 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 by about 1080 to 1130 psi relative to that during normal operation. The flow resistance would be expected to increase by over 60% (see the loss coefficients on Figure 6-1 at contact pressures of 2200 and 3300 psi), 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 6-2 at the bottom of the tubesheet. A comparison of the contact pressure during postulated SLB conditions relative to that during NOp is also provided for depths of 16.9, 12.6, 10.5 and 8.25 inches below the top of the tubesheet. The observations are discussed in the following.

- At the bottom of the tubesheet, Figure 6-2, the contact pressure increases by 1665 psi near the center of the tubesheet, exhibits no change at a radius of about 55 inches, and diminishes by 365 psi at the extreme periphery, a little less than 61 inches from the center.

- At 16.9 inches below the top of the tubesheet (a little over 4.1 inches from the bottom), the contact pressure increases by about 1085 psi at the center to a minimum of about 125 psi at a radius of 57 inches (See Figure 6-3). The contact pressure during a SLB is

everywhere greater than that during NOp. The influence of the channelhead and shell at the periphery causes the deformation to become non-uniform near the periphery.

- At a depth of 12.6 inches, Figure 6-4, the contact pressure increase ranges from a maximum of about 520 psi near the center of the tubesheet to 265 psi at a radius of 55 inches.

- At roughly the neutral surface, about 10.5 inches, Figure 6-5, the contact pressure during SLB is uniformly greater than that during normal operation by about 250 psi (ranging from 245 to 280 psi traversing outward).

- At a depth of 8.25 inches from the TTS, Figure 6-6, the contact pressure decreases by about 55 psi near the center of the TS to (Row 2) a maximum increase of 260 psi near the periphery.

- At a depth of about 6 inches from the TTS, Figure 6-7, the contact pressure decreases by about 365 psi at the center of the TS, is invariant at a radius of about 42 inches and increases by about 240 psi near the periphery.

The leak rate from any indication is determined by the total resistance of the crevice from the elevation of the indication to the top of the tubesheet in series with the crack itself, which is also expected to increase with contact pressure.[7] A comparison of the curves on Figure 6-6 relative to those on Figure 6-5 indicates that the contact pressure generally increases for a length of at least 2 inches upward from the mid-plane for all tubes.

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 6-2 at the bottom of the tubesheet show a decrease beyond a radius of 55 inches, however, the increase at 8.4 inches above the bottom, Figure 6-4 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 greater than 8 inches upward from an elevation of 4.26 inches above the bottom of the tubesheet 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 about 10 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 any Catawba 2 outage 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 meaningfully 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

[7] The effect of hoop compression on axial cracks would overwhelm the effect of the fluid pressure on the flanks.

during NOp at a depth of 6 inches from the TTS is far greater than the number for which the contact pressure decreases, i.e., 75% of the tubes are at a radius greater than 30 inches from the center of the tubesheet.

a,c,e

Figure 6-1. Loss Coefficient Values for Model F & D5 Leak Rate Analysis

a,c,e

Figure 6-2. Change in Contact Pressure at 21.0 inches Below the TTS

a,c,e

Figure 6-3. Change in Contact Pressure at 16.9 inches Below the TTS

a,c,e

Figure 6-4. Change in Contact Pressure at 12.6 inches Below the TTS

a,c,e

Figure 6-5. Change in Contact Pressure at 10.5 inches Below the TTS

a,c,e

Figure 6-6. Change in Contact Pressure at 8.25 inches Below the TTS

a,c,e

Figure 6-7. Change in Contact Pressure at 6.0 inches Below the TTS

7.0 Conclusions

The evaluation of Section 5.0 of this report provides a technical basis for assuring that the structural performance criteria of NEI 97-06 are inherently met for degradation of any extent below the H* depth identified in Table A-13, i.e., depths ranging from 2.33 to 8.61 inches below the BET or TTS whichever is lower selected to be bounding for all tubes in three particular zones. The corresponding evaluation presented in Section 6.0 provides 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 than twice the leak rate during normal operation. The conclusion also applies to any postulated indications in the tack expansion region and in the tube-to-tubesheet welds. 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 technical reason exists for a specific examination to take place.

The conclusions to be drawn from the evaluations performed are:

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 5.0 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) 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 SLB 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 a 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 in Section 6.0 of this report.

In conclusion, a relocation of the pressure boundary to 17 inches from the top of the tubesheet is acceptable from both structural and leak rate considerations. 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.

With regards 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 below 17 inches from 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 leak rate during a postulated SLB accident is not expected to increase beyond the SLB accident analysis leakage rate assumption of 0.1 gpm. In addition, the relocation of the pressure boundary does not create 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 will occur.

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Appendix A - 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 A.1. The construction code for the Catawba 2 SGs was the 1971 edition with the Winter 1972 and some paragraphs of the Winter 1974 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.

A summary discussion of the holding power of the tube-to-tubesheet joint is provided in Section 5.0 of this report. The conclusions of that discussion are that only 3.5 to 8.6 inches of engagement from the TTS and 1.5 to 2.2 inches of engagement above a 17 inch depth into the tubesheet are needed to resist all performance criteria axial loads.

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

A finite element model was developed for the Model D5 tubesheet, channel head, and shell region to determine the tubesheet hole dilations in the Catawba steam generators. [

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

A.1.1 Material Properties and Tubesheet Equivalent Properties

The tubes in the Catawba 2 SGs were fabricated of A600TT material. Summaries of the applicable mechanical and thermal properties for the tube material are provided in Table A-1. The tubesheets were fabricated from SA-508, Class 2a, material for which the properties are listed 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.4, and match the properties listed in the ASME Code.

The perforated tubesheet in the Model D5 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 A.5], 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:

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

with 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 1.0625 inches and nominal hole diameters are 0.764 inch. The ID of the tube after expansion into the tubesheet is taken to be 0.67886 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:

$h_{nominal}$ = $P_{nominal}$ - $d_{maximum}$
$P_{nominal}$ = 1.0625 inches, the pitch of the square hole pattern
$d_{maximum}$ = 0.764 inches, the maximum tube hole diameter

Therefore, $h_{nominal}$ = 0.2985 inches (1.0625-0.764), and η = 0.2809 when the tubes are not included. From Slot, Reference A.6, the in-plane mechanical properties for Poisson's ratio of 0.3 are:

Property	Value
$\overline{E}^*_p / E$	= 0.3992
$\overline{\nu}^*_p$	= 0.1636
$\overline{G}^*_p / G$	= 0.1674
E^*_z / E	= 0.5935
G^*_z / G	= 0.4189

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 A.7) elements used for the global model is σ_{xx}, σ_{yy}, τ_{xy}, and σ_{zz}. The mapping between the Reference A.5 equations and WECAN/+ is therefore:

Coordinate Mapping	
Reference A.5	WECAN/+
1	1
2	4
3	2
4	3

Table A-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 in the perforated region of the tubesheet. These are listed in Table A-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 A-1 for use in the calculations of the tube/tubesheet contact pressures.

A.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 D5 SG channel head/ tubesheet/ shell region (which includes the Catawba 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 A.7). The model for the D5 steam generator is shown on Figure A-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:

$$P_{endcap} = -\left[\frac{R_i^2}{R_o^2 - R_i^2}\right]P = -9708.43 \text{ psi}$$

where, R_i = Inside radius of secondary shell in finite element model = 64.69 in.
R_o = Outside radius of secondary shell in finite element model = 67.94 in.
P = Secondary pressure unit load = 1000 psi.

This yielded displacements throughout the tubesheet for the unit loads.

A.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 [

][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 A.8), 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 = 0.33943 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 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:

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

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

The resulting equation is:

a,c,e

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

The OD of the tubesheet cylinder is equal to that of the cylindrical (simulate) collars (1.80 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 A.9 and A.10).

The tube inside and outside radii within the tubesheet are obtained by assuming a nominal diameter for the hole in the tubesheet (0.764 inch) and wall thinning in the tube equal to the average of that measured during hydraulic expansion tests. That thickness is 0.04257 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.	0.33943
c, outside tube radius, in.	0.382
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.1.4 Catawba 2 Contact Pressures

A.1.4.1 Normal Operating Conditions

The loadings considered in the analysis are based on an umbrella set of conditions as defined in References A.5, A.6 and A.11. The current operating parameters from Reference A.12 are used. The temperatures and pressures for normal operating conditions at Catawba Unit 2 are bracketed by the following two cases:

Loading	T_{min} (1)	T_{max} (2)
Primary Pressure	2235 psig	2235 psig
Secondary Pressure	800 psig	771 psig
Primary Fluid Temperature (T_{hot})	603.4°F	603.4°F
Secondary Fluid Temperature	520.3°F	516.2°F

(1) T_{ave} Coast down with 0% Tube Plugging case in Reference A.12.
(2) T_{ave} Coast down with 10% Tube Plugging case in Reference A.12.

The primary pressure [

]^{a,c,e}.

A.1.4.2 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) are typically less than one-fourth of the tube lengths required to resist pull out during FLB and SLB (References A.8, A.9 and A.13. Therefore LOCA was not considered in this analysis.

A.1.4.2.1 Feedline Break

The temperatures and pressures for Feedline Break at Catawba Unit 2 are bracketed by the following two cases:

Loading	T_{ave} (1)	T_{ave} (2)
Primary Pressure	2835 psig	2835 psig
Secondary Pressure	0 psig	0 psig
Primary Fluid Temperature (T_{hot})	603.4°F	603.4°F
Secondary Fluid Temperature	520.3°F	516.2°F

(1) T_{ave} Coast down with 0% Tube Plugging case in Reference A.12.
(2) T_{ave} Coast down with 10% Tube Plugging case in Reference A.12.

The Feedline Break condition [

]^{a,c,e}.

A.1.4.2.2 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 hot leg temperature decreases throughout the transient, reaching a minimum temperature of 297°F at 2000 seconds for four 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})	=	297°F
Secondary Fluid Temperature	=	212°F

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

A.1.4.5 Summary of FEA Results for Tube-to-Tubesheet Contact Pressures

For Catawba 2, the contact pressures between the tube and tubesheet for various plant conditions are listed in Table A-6 and plotted versus radius on Figure A-3 through A-7. The application of these values to the determination of the required engagement length is discussed in Section A.2.

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

The elimination of a portion of the tube within the tubesheet from the in-service inspection requirement constitutes a change in the pressure boundary. This is the case regardless of whether or not the inspection is being eliminated in its entirety or if specialized probe examination is being eliminated when the potential for the existence of circumferential cracks is determined to be necessary for consideration. The elimination of the lower portion of the tube from examination is an H* partial-length specialized probe justification in the sense of Reference A.14 and relies on knowledge of the tube-to-tubesheet interfacial, mechanical interference fit contact pressure at all elevations in the tube joint. In order to maintain consistency with other reports on this subject, 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. Since the H* length is usually some distance from the top of the tubesheet, this is especially in the upper half of the tube joint. The contact pressure is used for estimating the magnitude of the anchorage of the tube in the tubesheet over the H* length. It is also used in estimating the impact of changes in the contact pressure on potential primary-to-secondary leak rate during postulated accident conditions.

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

]^{a,c,e} The residual contact pressure from the tube installation was evaluated semi-empirically. It was determined by test for the as-fabricated condition and then analytically projected to the pertinent plant conditions. The tests involved pullout testing of tube-in-tubesheet specimens using thick collars to simulate the tubesheet for the Model F tubes.

The end cap loads for Normal and Faulted conditions are:

Normal (maximum): $\pi \cdot (2235\text{-}796) \cdot (0.764)^2 / 4 = 671.15$ lbs.

Faulted (FLB): $\pi \cdot 2835 \cdot (0.764)^2 / 4 = 1299.66$ lbs.

Faulted (SLB): $\pi \cdot 2560 \cdot (0.764)^2 / 4 = 1173.59$ 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 A.17. Three (3) each of the tests were performed at room temperature, 400°F, and 600°F. (Note: Three other tests were performed with internal pressure in the tube. However, in these tests, the resistance to pullout was so great that the tube yielded, furnishing only input information of joint lower bound strength. These data were not used.) [

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[

]^{a,c,e}

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

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

where: F_{HE} = Resistance to pull out due to the initial hydraulic expansion,
d = Outside diameter of the tube in the tubesheet hole,
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 considered to vary linearly between adjacent elevations in the top part of Table A-7 through Table A-11, 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,

a,c,e

[ ]

so that,

a,c,e

[.]

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-7 through Table A-11 (with preload). The calculated values do include the axial resistance from the residual installation pressure. The above equation is also used to find the minimum contact lengths needed to meet the pullout force

requirements. In Zone C, the length calculated was 7.03 inches for the 3 times the normal operating pressure performance criterion which corresponds to a pullout force of 1979 lbf in the Hot Leg.

The top part of Table A-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 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-9. In Zone C, the respective length for the hot and cold legs is 8.1 and 8.6 inches for the 1.4 times the accident pressure performance criterion which corresponds to a pullout force of 1643 lbs in the Hot Leg for the SLB condition. The minimum contact length needed to meet the pullout force requirement of 1819 lb. for the FLB (feed line break) condition is less as is shown in Table A-10 and Table A-11. The H* calculations for each loading condition at each of the radii considered are summarized in Table A-12. The H* results for each zone are summarized in Table A-13.

Therefore, the bounding condition for the determination of the H* length is the SLB performance criterion. The minimum contact length for the SLB faulted condition is 8.13 or 8.61 inches in Zone C depending on tube leg. It is noted that the HL value reported in Reference A.16 for example is slightly higher owing to the use of a margin factor of 1.43 instead of the currently allowable value of 1.4.

In Zone B, The SLB performance criterion is controlling and the minimum contact length is 5.39 and 6.25 inches respectively for the hot and cold legs. In Zone A, however, the normal operating condition is controlling and the corresponding minimum contact lengths are calculated to be 2.33 and 3.45 inches.

The conditions on the cold leg lead to conclusions similar to those for the hot leg of the SG. There are competing effects that influence the determination of the required engagement length. During normal operation:

(1) The tube and tubesheet temperature are less than on the hot leg, which leads to a reduction in the thermal expansion contribution to the total contact pressure.

(2) The internal pressure in the tube is also less (albeit a small amount), leading to a reduction its contribution to the total contact pressure.

(3) This latter effect also leads to a small reduction in the differential pressure induced end-cap load acting to pull and push a postulated severed tube out of the tubesheet.

(4) Finally, the magnitude of the bowing deformation of the tubesheet is reduced, meaning that the reduction of the contact pressure associated with tubesheet deflection is also diminished.

The net effect is an increase in the calculated value of H* during normal operation of about 1.1 inches at the maximum location, and an increase of about 0.5 inch near the periphery. The values obtained near the center of the TS are determined by NOp conditions and the values outboard of about 34 inches are determined by the SLB conditions.

A.3 References

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A.16 WNET-153 (Proprietary), "Model D05 Steam Generator Stress Report: Tube Analysis," Westinghouse Electric Company LLC, Pittsburgh, PA, March 1982.

A.17 LTR-CDME-05-180-P, "Steam Generator Tube Alternate Repair Criteria for the Portion of the Tube Within the Tubesheet at Catawba Unit 2," December 2005.

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·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 A-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 A-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 A-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 A-5. Equivalent Solid Plate Elasticity Coefficients for D5 Perforated TS
SA-508 Class 2a Tubesheet Material (10^6 psi)

a,c,e

Table A-6. Tube/Tubesheet Maximum & Minimum Contact Pressures & H*
for Catawba Unit 2 Steam Generators

a,c,e

Table A-7. Cumulative Forces Resisting Pull Out from the TTS Catawba 2 Hot Leg Normal Conditions T_{ave} Coastdown with 0% Plugging

a,c,e

Table A-8. Cumulative Forces Resisting Pull Out from the TTS Catawba 2 Hot Leg Normal Conditions T_{ave} Coastdown with 10% Plugging

a,c,e

Table A-9. Cumulative Forces Resisting Pull Out from the TTS Catawba 2 Faulted (SLB) Conditions

a,c,e

Table A-10. Cumulative Forces Resisting Pull Out from the TTS Catawba 2 FLB Conditions Coastdown with 0% Plugging

a,c,e

Table A-11. Cumulative Forces Resisting Pull Out for FLB Conditions
T_{ave} Coastdown with 10% Plugging

a,c,e

Table A-12. Summary of H* Calculations for Catawba Unit 2

a,c,e

Table A-13. H* Summary Table		
Zone	Limiting Loading Condition	Engagement from TTS (inches)
A	3.0 NO ΔP [1,2]	HL 2.33 [3] CL 3.45
B	1.4 SLB ΔP [1,2]	HL 5.39 CL 6.25
C	1.4 SLB ΔP [1,2]	HL 8.13 CL 8.61

Notes:

1. Seismic loads have been considered and are not significant in the tube joint region (Reference A.16).
2. The scenario of tubes locked at support plates is not considered to be a credible event in Model D5 SGs as they are manufactured with stainless steel support plates. However, conservatively assuming that the tubes become locked at 100% power conditions, the maximum force induced in an active tube as the SG cools to room temperature is
   [
   ]a,c,e
3. 0.3 inches 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 A-1. Definition of H* Zones

a,c,e

Figure A-2. Finite Element Model of Model D5-3 Tubesheet Region

a,c,e

Figure A-3. Contact Pressures for Normal Condition (10% SGTP) at Catawba 2

a,c,e

Figure A-4. Contact Pressures for Normal Condition (0% SGTP) at Catawba Unit 2

a,c,e

Figure A-5. Contact Pressures for SLB Faulted Condition at Catawba 2

a,c,e

Figure A-6. Contact Pressures for FLB Condition at Catawba 2 T_{ave} Coastdown 0% Plugging

a,c,e

Figure A-7. Contact Pressures for FLB Condition at Catawba 2 T_{ave} Coastdown 10% Plugging