Attachment 6

BYRON STATION
UNITS 1 AND 2

Docket Nos. 50-454 and 50-455

License Nos. NPF-37 and NPF-66

Request for License Amendment Related to Technical Specification 5.5.9, "Steam Generator (SG) Tube Surveillance Program"

Westinghouse Electric Company LTR-CDME-05-32-NP, "Limited Inspection of the Steam Generator Tube Portion Within the Tubesheet at Byron 2 & Braidwood 2," Revision 1, dated May 2005

Non-Proprietary Version

LTR-CDME-05-32-NP, Rev. 1

Limited Inspection of the Steam Generator Tube Portion Within the Tubesheet at Byron 2 & Braidwood 2

May 2005

Author: /s/ Robert F. Keating
Robert F. Keating
Major Component Replacements & Engineering

Verified: /s/ Hermann O. Lagally
Hermann O. Lagally
Chemistry Diagnostics & Materials Engineering

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

This page intentionally blank.

Revision Log

Revision	Changes
0	(original issue)
1	General: - Minor typographical corrections - Added new reference as Reference 9; other references indexed accordingly Section 1, Introduction - Added "diameter and thickness" in 2nd para. - Changed 4.23 to 4.26 and 21.23 to 21.26 in 4th para. - Added "including some extending into the tube" in 3) - Deleted "exigent"; revised last sentence in 6th para. - Replaced "remaining in" with "below" in last sentence, 7th para. Section, 3 Historical Background.... - Changed Reference 15 to 17 in 2nd para. Section 5, Structural Analysis... - Added reference 14 in 2nd para. Section 6.0, Leak Rate Analysis... - Added "and the fall 2005 outage for Byron 2" after Braidwood 2.

This page intentionally blank.

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 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 recent requirements interpretations published by the NRC staff in GL 2004-01, Exelon requested that a recommendation be developed for examination of the Westinghouse Model D5 steam generator tubesheet regions at the Byron 2 and Braidwood 2 power plants. An evaluation was performed that considered the requirements of the ASME Code, Regulatory Guides, NRC Generic Letters, NRC Information Notices, the Code of Federal Regulations, NEI 97-06, and additional industry requirements. The conclusion of the technical evaluation is that the structural 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 thick tubesheet or the tube end welds because the tube-to-tubesheet hydraulic 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 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-16152 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.

Table of Contents

Revision Log 3

Abstract 5

1.0 Introduction 9

2.0 Summary Discussion 12

3.0 Historical Background Regarding Tube Indications in the Tubesheet 14

4.0 Design Requirements for the Tube-to-Tubesheet Joint Region 15

5.0 Structural Analysis of Tube-to-Tubesheet Joint 16

6.0 Leak Rate Analysis of Cracked Tube-to-Tubesheet Joints 18

6.1 The Bellwether Principle for Normal Operation to Steam Line Break Leak Rates 18

6.2 Leakage Analysis from H* Calculations for Comparison 22

7.0 Recommendations for Dispositioning Tube Cracks in the Tube-to-Tubesheet Joint 23

8.0 Conclusions 23

9.0 Recommended Inspection Plans 24

10.0 References 26

Appendix A - Structural Analysis of the Tube-to-Tubesheet Contact Pressure 34

A. Structural Analysis of the Tube-to-Tubesheet Interface Joint 34

A.1 Evaluation of Tubesheet Deflection Effects for Tube-to-Tubesheet Contact Pressure 34

A.1.1 Material Properties and Tubesheet Equivalent Properties 34

A.1.2 Finite Element Model 37

A.1.3 Tubesheet Rotation Effects 38

A.1.4 Byron/Braidwood 2 Contact Pressures 41

A.1.5 Summary of FEA Results for Tube-to-Tubesheet Contact Pressures 43

A.2 Determination of Required Engagement Length of the Tube in the Tubesheet 43

A.3 References 46

List of Tables

Table A.1: Summary of Material Properties Alloy 600 Tube Material 49

Table A.2: Summary of Material Properties for SA-508 Class 2a Tubesheet Material 49

Table A.3: Summary of Material Properties SA-533 Grade A Class 2 Shell Material 49

Table A.4: Summary of Material Properties SA-216 Grade WCC Channelhead Material 50

Table A.5: Equivalent Solid Plate Elasticity Coefficients for D5 Perforated TS SA-508 Class 2a Tubesheet Material 51

Table A.6: Tube/Tubesheet Maximum & Minimum Contact Pressures & H* for Byron/Braidwood Unit 2 Steam Generators 51

Table A.7: Cumulative Forces Resisting Pull Out from the Top of the Tubesheet Byron/Braidwood 2 – Hot Leg Normal Conditions Low T_{ave}, High T_{sec} 52

Table A.8: Cumulative Forces Resisting Pull Out from the TTS Byron/Braidwood 2 – Hot Leg Normal Conditions 53

Table A.9: Cumulative Forces Resisting Pull Out from the TTS Byron/Braidwood 2–Faulted (SLB) Conditions 54

Table A.10: Cumulative Forces Resisting Pull Out from the TTS Byron/Braidwood 2–FLB Conditions Low T_{ave}, High T_{sec} 55

Table A.11: Cumulative Forces Resisting Pull Out for FLB Conditions 56

Table A.12: Large Displacement, 0.25 in. Pullout Test Data 57

Table A.13: Summary of H* Calculations for Byron/Braidwood Unit 2 57

Table A.14: H* Summary Table 58

List of Figures

Figure 1: Distribution of Indications in SG A at Catawba 2 28
Figure 2: Distribution of Indications in SG B at Catawba 2 28
Figure 3: Distribution of Indications in SG D at Catawba 2 29
Figure 4: As-Fabricated & Analyzed Tube-to-Tubesheet Welds 29
Figure 5: Definition of H* Zones from Reference 5 30
Figure 6: Flow Resistance Curve from Reference 5 30
Figure 7: Change in contact pressure at 10.5 inches below the TTS 31
Figure 8: Change in contact pressure at 12.6 inches below the TTS 31
Figure 9: Change in contact pressure at 16.9 inches below the TTS 32
Figure 10: Change in contact pressure at the bottom of the tubesheet 32
Figure 11: Change in contact pressure at 8.25 inches below the TTS 33
Figure A.1: Finite Element Model of Model D5-3 Tubesheet Region 59
Figure A.2: Contact Pressures for Normal Condition (T_{min}) at Byron/Braidwood 2 60
Figure A.3: Contact Pressures for Normal Condition (T_{max}) at Byron and Braidwood Unit 2 60
Figure A.4: Contact Pressures for SLB Faulted Condition at Byron and Braidwood 2 61
Figure A.5: Contact Pressures for FLB Faulted Condition at Byron and Braidwood 2 (T_{min}) 61
Figure A.6: Contact Pressures for FLB Faulted Condition at Byron and Braidwood 2 (T_{max}) 62
Figure A.7: D5 Pullout Test Results for Force/inch at 0.25 inch Displacement 62

Limited Steam Generator Tube-in-Tubesheet
Inspection at Byron 2 & Braidwood 2

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 tube indications at Catawba were reported about 7.6 inches from the top of the tubesheet in one tube, and just above the tube-to-tubesheet welds in a region of the tube known as the tack expansion (TE) in several other tubes. Finally, indications were also reported in the tube-end welds (TEWs), also known as tube-to-tubesheet welds, joining the tube to the tubesheet. The spatial distribution by row and column number is shown on Figure 1 for SG A, Figure 2 for SG B, and Figure 3 for SG D at Catawba. There were no indications in SG C. The Catawba 2 plant has Westinghouse designed, Model D5 SGs similar to those in service at the Exelon Corporation's Byron Unit 2 and Braidwood Unit 2 plant sites. Model D5 SGs were fabricated with Alloy 600TT (thermally treated) tubes. Although the remaining other plant site with Westinghouse Model D5 SGs, which belongs to another utility, has not reported similar indications, it is believed that no RPC (rotating probe coil) inspection of the tube region in the vicinity of the tack expansions or the tube-to-tubesheet welds with inspection techniques other than visual examination using SG bowl cameras has been performed. In other words, eddy current test (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. This experience is similar to that at the Braidwood 2 and Byron 2 plant sites. Thus, there is a potential for tube indications similar to those reported at Catawba within the tubesheet region to be reported in the Braidwood 2 and Byron 2 SGs if similar inspections were to be performed during the spring and fall 2005 inspections of their respective SGs. (Note: No indications were found during the planned inspection of the Braidwood 2 SG tubes in April 2005 as described herein. Moreover, no indications were found during a somewhat similar inspection of the tubes in two SGs at Wolf Creek in April 2005.)

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. The Vogtle SGs are of the Westinghouse Model F design with slightly smaller, diameter and thickness, A600TT tubes.

The SGs for all four Model D5 plant sites were fabricated in the 1978 to 1980 timeframe using similar manufacturing processes with a few exceptions. For example, the fabrication technique used for the installation of the SG tubes at Braidwood 2 would be expected to lead to a much lower likelihood for crack-like indications to be present in the region known as the tack expansion relative to Catawba 2 because a different process for effecting the tack expansions was adopted prior to the time of the fabrication of the Braidwood 2 SGs. The same statement cannot be made with regard to the tack expansion region in the Byron 2 SGs since they were fabricated at about the same time as the Catawba 2 SGs using the same tack expansion process.

A recommended examination plan for the tubes and welds is delineated in Section 9.0 of this report. 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 the SG tubes at either the Byron 2 or Braidwood 2 plant sites. Exelon is not attempting to license the H* methodology as described in Reference 5 for application to the tubes in the Byron 2 and Braidwood 2 SGs, but the structural analysis of the tube and the tubesheet documented in that reference is valid for use in supporting the application of a recently developed 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, Exelon decided to increase the depth of RPC inspection of the tubes to 17 inches from the top of tubesheet (TTS). This allows the use of the newly developed leak rate methodologies since excluded potential degradation regions would be limited to the bottom 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 Byron 2 and Braidwood 2 plants:

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 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 an H* type of 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 the spring 2005 inspection of the Braidwood 2 (completed as of the time of Revision 1 to this document) and the fall 2005 of the Byron 2 SGs, a technical specification, a.k.a. the TS, amendment is being sought; the requested change was approved for Braidwood 2 in April 2005, Reference 9. 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 to 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, Exelon is not attempting to license H*, but to use data 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 the 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 1, Reference 10, 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 Byron 2 and Braidwood 2 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 Braidwood 2 for the spring 2005 outage and Byron 2 for the fall 2005 outage 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. However, there are no technical reasons why the use of the alternate methodology should be limited to a single application at either plant site.

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 Byron 2 and Braidwood 2 power plants. The conclusions from the evaluation are that a 20% sample of the tube 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. Exelon has decided to perform sampling RPC inspections to a depth of 17 inches below the top of the tubesheet for the spring 2005 inspection of the Braidwood 2 SGs and the fall 2005 inspection at Byron 2 in order to assure that leakage requirements in addition to the structural requirements are met.

It is noted that the above inspection recommendation 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 4.23 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 demonstration of compliance with leak rate limits during postulated faulted events.

In summary:

- WCAP-16152, Reference 5, notes that the structural integrity requirements of NEI 97-06, Reference 10, and draft RG 1.121, Reference 11, are met by sound tube engagement lengths ranging from 2.95 to 8.61 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 10, 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 Code, Summer 1972 Addenda and selected paragraphs of the Winter 1974 Addenda, Reference 7. The analyses are

documented in References 13 and 14 for the Byron 2 and Braidwood 2 SGs respectively. The typical as-fabricated and the as-analyzed weld configurations are illustrated on Figure 4.

- Section XI of the ASME Code, Reference 15 (1971) through 16 (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 D5 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-16152, 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, the attendant accident condition leak rate, assuming all leakage to be from lower tubesheet indications, would be bounded by 0.2 gpm. 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." 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 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 considered to be necessary to assure 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. It is believed that the rolling process was used during fabrication of the Byron Unit 2 SGs, while the urethane plug (Poisson) expansion process was used for those at Braidwood Unit 2. The tube-to-tubesheet weld was then performed to create the ASME Code pressure boundary between the tube and the tubesheet.[1]

The development of the F* alternate repair criterion (ARC) in 1985-1986 for tubes hard rolled into the tubesheet was prompted by the desire to account for the inherent strength of the tube-to-tubesheet joint away from the weld and to allow tubes with degradation within the tubesheet to remain in service, Reference 17. 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 18. The W* criteria were first applied to operating SGs in 1999 based on a generic evaluation for Model 51 SGs, Reference 19, and the subsequent safety evaluation by the NRC staff, Reference 20. However, the 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.

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

The post-weld expansion of the tube into the tubesheet in the Byron 2 and Braidwood 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 Byron 2 and Braidwood 2 SGs, on the structural and leakage integrity of the joint, Reference 21. 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 10 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 10. 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 10):

> *"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 (American Society of Mechanical Engineers) Boiler and Pressure Vessel Code, Reference 7. The construction code for the Byron and Braidwood Unit 2 SGs was the 1971 edition with the Summer 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.

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 Byron 2 and Braidwood 2 Model D5 SGs and the results were reported in Westinghouse report WCAP-16152, Reference 5; again, the structural analysis section of that reference is included herein as Appendix A. Typical Model D5 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. 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 were used to demonstrate that engagement lengths of approximately 3 to 8.6 inches were sufficient to equilibrate the axial loads resulting from consideration of 3 times the normal operating and 1.4 times the limiting accident condition pressure differences. The variation in required engagement length is a function of tube location, i.e., row and column, and decreases away from the center of the SG where the maximum value applies. The tubesheet bows, i.e., deforms, upward from the primary-to-secondary pressure difference and results in the tube holes becoming dilated above the neutral plane of the tubesheet, which is 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[2]. 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 References 13 and 14, and the requirements of RG 1.121, Reference 11.

An examination of Tables A.7 through A.11 illustrates 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 the entire radial zones of consideration as defined on Figure 5 (taken from Reference 5). 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, 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 4.9 inch range from 12 to 16.9 inches below the top of the tubesheet is about 1191 lbf per inch during normal operation. The performance criterion for $3 \cdot \Delta P$ is met by the first 1.7 inch of engagement above 17 inches. At a radius of 59 inches the corresponding length of engagement needed is about 2.1 inches. The corresponding values for steam line break conditions are 1.07 and 1.69 inches at radii of 4.08 and 58.8 inches respectively. In other words, while a value of 8.6 inches was determined for H* from the top of the tubesheet, a length of 1.7 to 1.85 inches would be sufficient at the bottom of the inspection length, where the latter value corresponds to a radius of 34.4 inches from the center of the tubesheet, the maximum extent of Zone C.

[2] 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. Although the hydraulically expanded joint is not leak tight, 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 Byron 2 and Braidwood 2 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. Moreover, 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 noted that the degradation reported in the Catawba 2 and Vogtle 1 SG tubes was bounded (significantly) by the degradation extent specified for application by Reference 5. Moreover, 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 spring 2005 outage for Braidwood 2 and the fall 2005 outage for Byron 2. (The approval for the change to the Braidwood 2 Technical Specification was obtained in April 2005.) 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 diminution 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 of 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. The results from these considerations were not included in the final versions of the document because of concerns associated with the accuracy of the approach for indications 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.[3] 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 diminution of the holes below the neutral plane presents definitive statements to be made with regard to the trend of the leak rate, hence, the bellwether principle. Independent consideration of the effect of the tube-to-tubesheet contact pressure leads to similar conclusions with regard to the opening area of the cracks in the tubes, thus further restricting the leak rate beyond that through the interface between the tube and the tubesheet.

In order to accept the concept of normal operation being a bellwether for the postulated accident leak rate for indications above the neutral plane of the tubesheet, the change in leak rate had to be quantified using a somewhat complex, physically sound model of the thermal-hydraulics of the leak rate phenomenon. This is not necessarily the case for cracks considered to be present below the neutral plane of the tubesheet. This is because a diminution of the holes takes place during postulated accident conditions below the neutral plane relative to normal operation. For example, at a radius of approximately 34 inches from the center of the SG, the contact pressure during normal operation is calculated to be

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

about 2010 to 2200 psi[4], see the last contact pressure entry in the center columns of Table A.8 and Table A.7 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 4250 to 4290 psi at the bottom of the tubesheet, Table A.10 and Table A.11 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 4.08 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) indicates that the resistance to flow (the loss coefficient) would increase by a factor of about 3. If the leak rate during normal operation was 0.104 gpm (150 gpd), the postulated accident condition leak rate would be on the order of 0.2 gpm considering only the change in differential pressure, but the estimate would be reduced to 0.07 gpm when the increase in contact pressure is included, i.e., about 70% of that during normal operation based on the factor of 3. This latter value is significantly less than the allowable limit during faulted conditions of 0.5 gpm at room temperature density. Even without inclusion of the effect of the change in contact pressure, the predicted leak rate would be significantly less than the allowable rate of 0.5 gpm.

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 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.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. At the central plane of the tubesheet the increase in contact pressure is more on the order of 15% 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 50% 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

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

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 270 psi (ranging from 255 to 291 psi).
- At a depth of 12.6 inches the contact pressure increase ranges from a maximum of 537 psi near the center of the tubesheet to 275 psi at a radius of 55 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 821 psi to a minimum of 236 psi at a radius of 56 inches, Figure 9.
- At the bottom of the tubesheet, Figure 10, the contact pressure increases by over 1700 psi near the center of the tubesheet, exhibits no change at a radius of about 55 inches, and diminishes by 369 psi at the extreme periphery, a little less than 61 inches from the center.

At a depth of about 6 inches from the top of the tubesheet the contact pressure decreases by about 370 psi near the center of the tubesheet, is unchanged at a radius of about 42 inches and increases by a maximum of 251 psi at a radius of 58 inches. 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 decreases at most by 53 psi at a radius of 3.1 inches, is unchanged at a radius of 21 inches, and increases by a maximum of 268 psi at a radius of 56.9 inches. The density of the number of tubes populating the tubesheet increases with the square of the radius, thus, even 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., 88% of he tubes are at a radius greater than 21 inches from the center of the tubesheet.

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, 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 a little deeper into the tubesheet. For example, it would be expected 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 11 relative to those on Figure 7 indicates that the contact pressure generally increases for a length of at least 2 inches upward from the mid-plane for tubes with a radius of 21 inches from the center of the tubesheet. For radial locations greater than 21 inches from the center of the tubesheet the length for which the contact pressure increases would be greater than 2 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 55 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 8 inches upward from an elevation of 4.23 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 17 inches from the top of the tubesheet. Therefore, applying a very conservative inspection sampling length of 17 inches downward from the top of the tubesheet during the Braidwood 2 spring 2005 and the Byron 2 fall 2005 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-16152, 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 Exelon 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 D5 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 0.195 gpm or about 40% 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 Braidwood 2 spring 2005 and the Byron 2 fall 2005 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 spring and fall 2005 outages at Braidwood 2 and Byron 2 respectively 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 than 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 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 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 Braidwood 2 and Byron 2 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.

Based on the summary discussion, Westinghouse has reviewed and endorses the following SG tubes inspection plan with regard to the tubesheet region in each SG as discussed with Exelon (Messrs. M. Sears and S. Leshnoff) on March 21 through April 9, 2005, for the Braidwood 2 and Byron 2 spring and fall 2005 outages respectively:

1. Perform 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 in this region only if cracking is found that is not associated with a bulge or overexpansion as described below.
2. Perform a 20% inspection of the hot leg side tubes using RPC technology for depths indicated down to the top of the tubesheet minus 17 inches for indications of bulges ≥ 18 Volts and over expansions ≥ 1.5 mils on the diameter, as obtained from a review of the cycle 10 data for Braidwood 2 and cycle 11 data for Byron 2. Note, the 20% sample could be developed by examining 20% of the parent tube population and biasing the selection process to assure that at least 20% of the bulge and over expansion indications within the 17 inch length were also sampled. It is also noted that 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 minus 17 inches in the affected SG.

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.

10.0 References

1. OE19662 (Restricted & Confidential), "Steam Generators (Catawba Nuclear Power Station)," Institute of Nuclear Power Operations (INPO), Atlanta, GA, USA, December 13, 2004.
2. IN 2005-09, "Indications in Thermally Treated Alloy 600 Steam Generator Tubes and Tube-to-Tubesheet Welds," United States Nuclear Regulatory Commission, Washington, DC, publication date to be determined, April 7, 2005.
3. SGMP-IL-05-01, "Catawba Unit 2 Tubesheet Degradation Issues," EPRI, Palo Alto, CA, March 4, 2005.
4. OE20339, "Vogtle Unit 1 Steam Generator Tube Crack Indications," Institute of Nuclear Power Operations (INPO), Atlanta, GA, USA, April 4, 2005.
5. WCAP-16152, "Justification of the Partial-Length Rotating Pancake Coil (RPC) Inspection of the Tube Joints of the Byron/Braidwood Unit 2 Model D5 Steam Generators," Westinghouse Electric Company LLC, Pittsburgh, PA, December 2003.
6. 1003138, "EPRI PWR SG Examination Guidelines: Revision 6," EPRI, Palo Alto, CA, October 2002.
7. ASME Boiler and Pressure Vessel Code, Section III, "Nuclear Power Plant Components," American Society of Mechanical Engineers, New York, New York, 1971, Summer 1972 Addenda and selected sections of the Winter 1974 addenda.
8. GL 2004-01, "Requirements for Steam Generator Tube Inspections," United States Nuclear Regulatory Commission, Washington, DC, August 30, 2004.
9. NRC Letter, "Braidwood Station, Units 1 and 2 – Issuance of Exigent Amendments Re: Revision of Scope of Steam Generator Inspections for Unit 2 Refueling Outage 11 – (TAC Nos. MC6686 and MC6687)," United States Nuclear Regulatory Commission, Washington, DC, April 25, 2005.
10. NEI 97-06, Rev. 1, "Steam Generator Program Guidelines," Nuclear Energy Institute, Washington, DC, October 2000.
11. RG 1.121 (Draft), "Bases for Plugging Degraded PWR Steam Generator Tubes," United States Nuclear Regulatory Commission, Washington, DC, August 1976.
12. Federal Register, Part III, Nuclear Regulatory Commission, National Archives and Records Administration, Washington, DC, pp. 10298 to 10312, March 2, 2005.
13. WNET-153 (Proprietary), "Model D5 Steam Generator Stress Report Commonwealth Edison Company Byron Station 2," Westinghouse Electric Company LLC, Pittsburgh, PA, June 1982.
14. WNEP-8425 (Proprietary), "Model D5 Steam Generator Stress Report for Commonwealth Edison Company Braidwood Unit 2," Westinghouse Electric Company LLC, Pittsburgh, PA, March 1984.

15. ASME Boiler and Pressure Vessel Code, Section XI, "Rules for Inservice Inspection of Nuclear Power Plant Components," American Society of Mechanical Engineers, New York, New York, 1971.

16. ASME Boiler and Pressure Vessel Code, Section XI, "Rules for Inservice Inspection of Nuclear Power Plant Components," American Society of Mechanical Engineers, New York, New York, 2004.

17. WCAP-11224, Rev. 1, "Tubesheet Region Plugging Criterion for the Duke Power Company McGuire Nuclear Station Units 1 and 2 Steam Generators," Westinghouse Electric Company LLC, Pittsburgh, PA, October 1986.

18. WCAP-13532, Rev. 1, "Sequoyah Units 1 and 2 W* Tube Plugging Criteria for SG Tubesheet Region of WEXTEX Expansions," Westinghouse Electric Company LLC, Pittsburgh, PA, 1992.

19. WCAP-14797, "Generic W* Tube Plugging Criteria for 51 Series Steam Generator Tubesheet Region WEXTEX Expansions," Westinghouse Electric Company LLC, Pittsburgh, PA, 1997.

20. "Safety Evaluation by the Office of Nuclear Reactor Regulation Related to Amendment No. 129 to Facility Operating License No. DPR-80 and Amendment No. 127 to Facility Operating License No. DPR-82 Pacific Gas and Electric Company Diablo Canyon Nuclear Power Plant, Units 1 and 2 Docket Nos. 50-275 and 50-323," United States Nuclear Regulatory Commission, Washington, DC, 1999.

21. NSD-RFK-96-015, "Vogtle 1 Tube Integrity Evaluation, Loose Part Affected SG," Westinghouse Electric Company LLC, Pittsburgh, PA, June 9, 1996.

22. WNEP-8448 (Proprietary), "Hydraulic Expansion of SG Tubes Into Tubesheets," Westinghouse Electric Company LLC, Pittsburgh, PA, December 1983.

23. NEI Letter, "Steam Generator Tube Inspection Generic Letter (GL 2004-01) Response," Nuclear Energy Institute, Washington, DC, October 15, 2004.

24. 10CFR50 (Title 10, Part 50 of the Code of Federal Regulations, "Energy, Containing a Codification of Documents of General Applicability and Future Effect," Office of the Federal Register, National Archives and Records Administration, Washington, DC 20408, January 2004.

25. RG 1.83, Rev. 1, "Inservice Inspection of Pressurized Water Reactor Steam Generator Tubes," United States Nuclear Regulatory Commission, Washington, DC, July 1975,

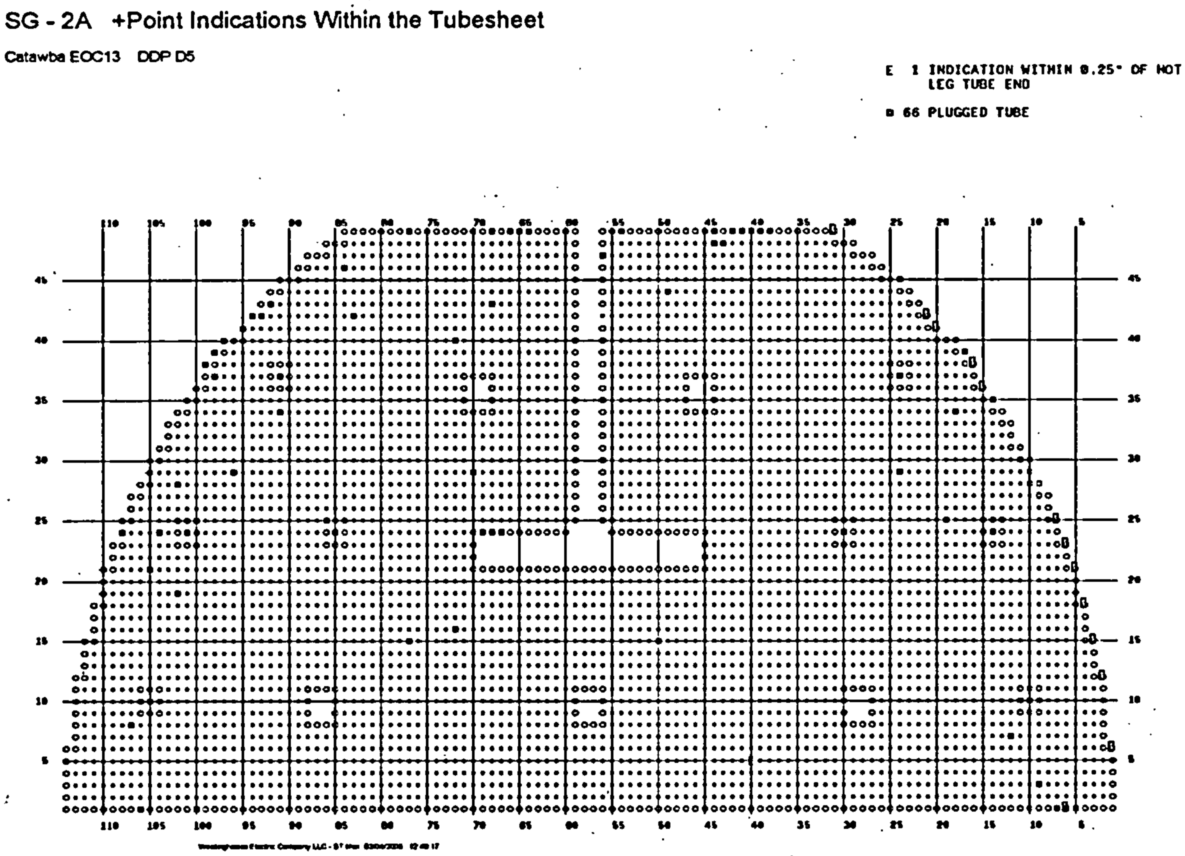


Figure 1: Distribution of Indications in SG A at Catawba 2

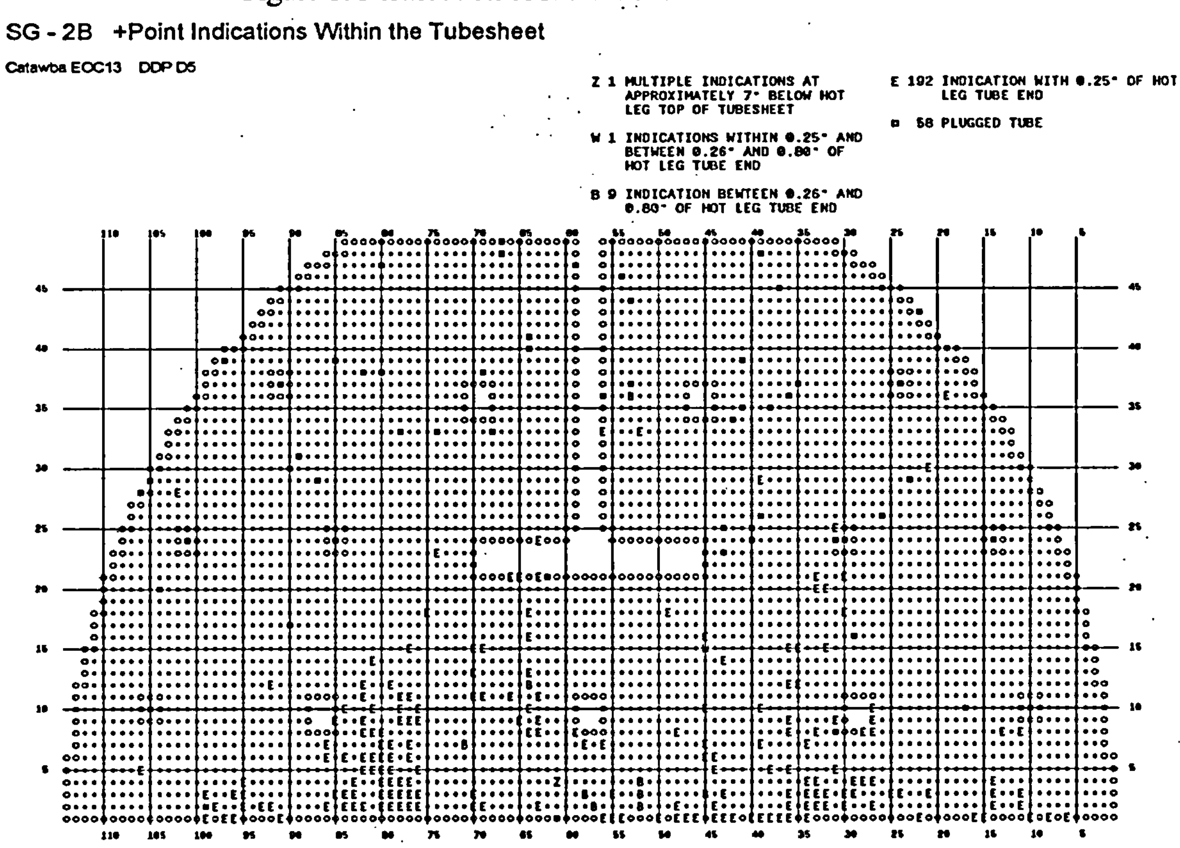


Figure 2: Distribution of Indications in SG B at Catawba 2

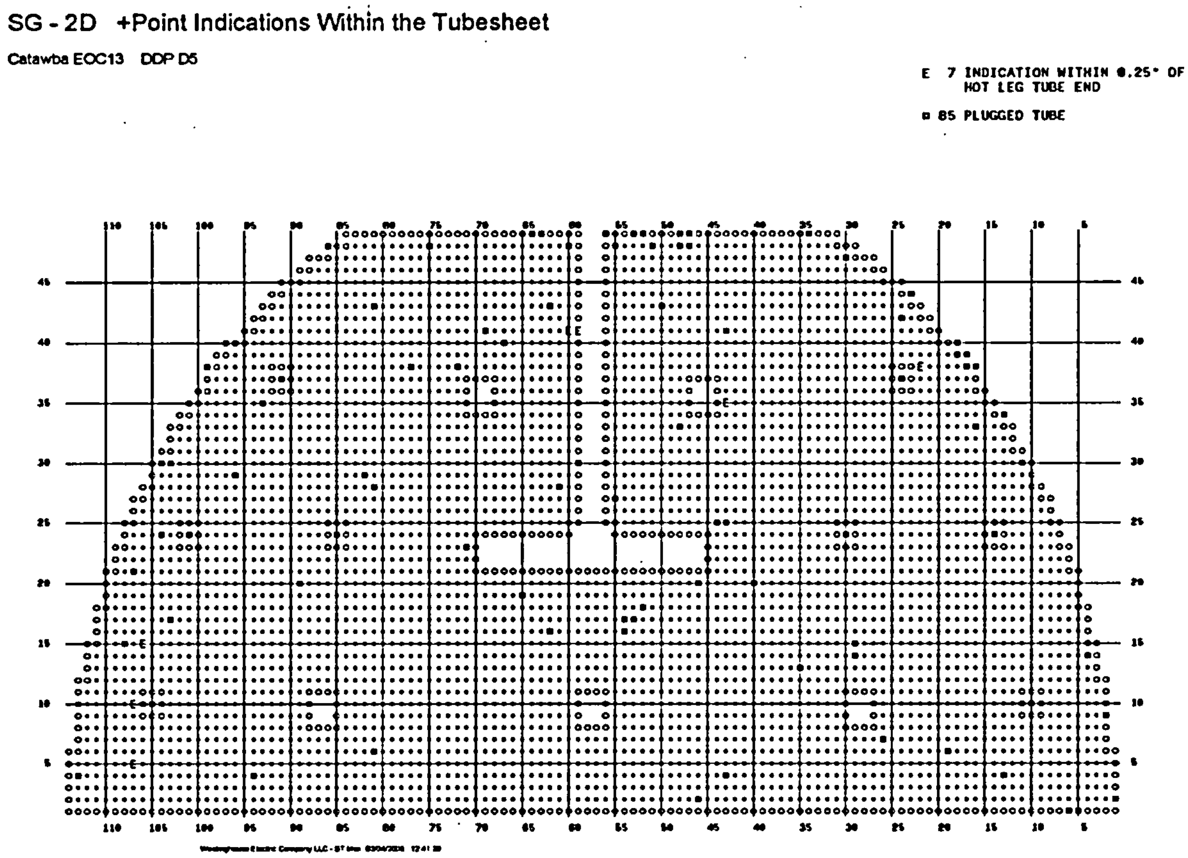
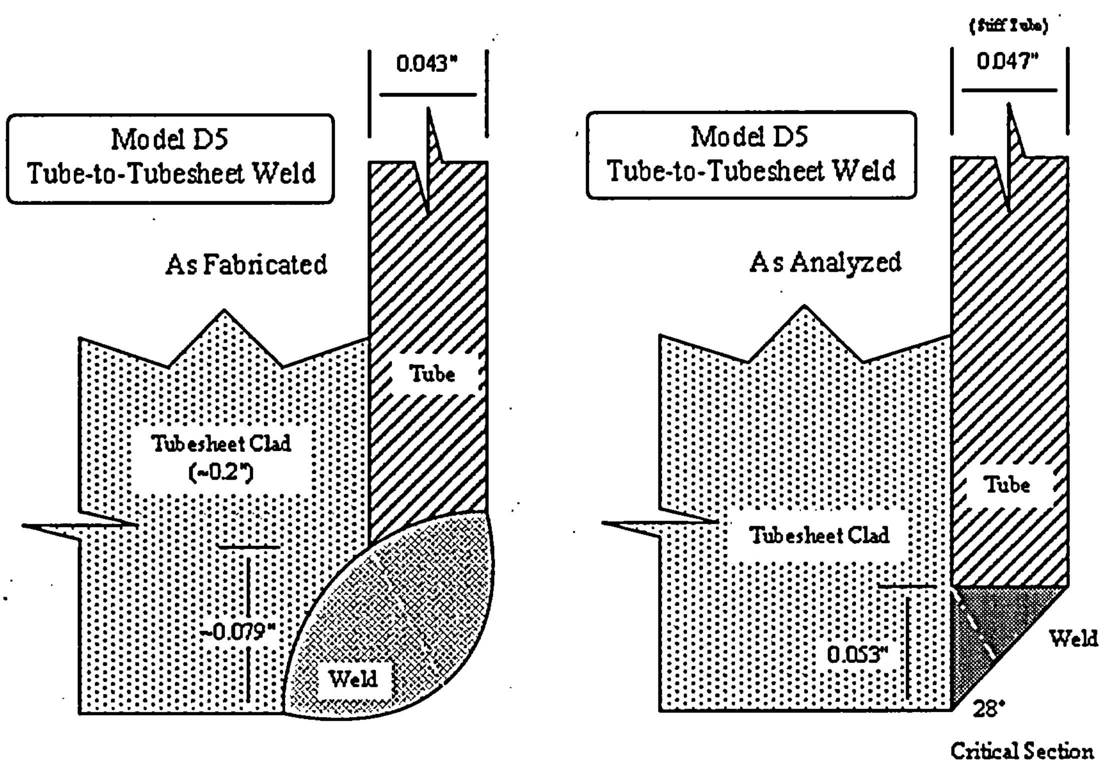


Figure 3: Distribution of Indications in SG D at Catawba 2



a,c,e

Figure 5: Definition of H* Zones from Reference 5

a,c,e

Figure 6: Flow Resistance Curve from 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 at the bottom of the tubesheet

a,c,e

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

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

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

An evaluation was performed to determine the contact pressures between the tubes and the tubesheet in the Byron 2 & Braidwood 2 SGs to support the determination of the engagement length needed to resist the performance criteria end cap loads and estimation of potential leak rates through the tube-to-tubesheet joints. The evaluation utilized [

]^{a,c,e}, were determined.

The same contact pressure results were used in the bellwether analysis to establish bounding values for the potential leak rate during postulated accident conditions relative to that during normal operation.

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 Byron/Braidwood steam generators. [

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

A.1.1 Material Properties and Tubesheet Equivalent Properties

The tubes in the Byron 2 and Braidwood 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-12], 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

$d_{maximum}$ = .764 inches, the 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-13, 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-14) elements used for the global model is σ_{xx}, σ_{yy}, τ_{xy}, and σ_{zz}. The mapping between the Reference A-12 equations and WECAN/+ is therefore:

Coordinate Mapping	
Reference A-12	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 D5-2 SG channel head/tubesheet/shell region (which includes the Byron/Braidwood 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-14). The model for the D5-2 steam generator is shown in Figure A.1.

The unit loads applied to this model is 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:

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

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-15), 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:

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

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

The resulting equation is:

a,c,e

[ ]

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

The OD of the tubesheet cylinder is equal to that of the cylindrical (simulate) collars (1.80 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 (Reference A-16).

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 Byron/Braidwood 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-11 through A-13. The current operating parameters from Reference A-2 are used. The temperatures and pressures for normal operating conditions at Byron/Braidwood Unit 2 are bracketed by the following two cases:

Loading	T_{min} (1)	T_{max} (2)
Primary Pressure	2235 psig	2235 psig
Secondary Pressure	796 psig	938 psig
Primary Fluid Temperature (T_{hot})	608.0°F	620.3°F
Secondary Fluid Temperature	519.8°F	538.8°F

(1) Low T_{ave} with 10% Tube Plugging case in Reference A-2.
(2) High T_{ave} with 0% Tube Plugging case in Reference A-2.

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) typically less than one-fourth of the tube lengths required to resist pull out during FLB and SLB (References A-15 and A-17). Therefore LOCA was not considered in this analysis.

A.1.4.2.1 Feedline Break

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

Loading	T_{min} (1)	T_{max} (2)
Primary Pressure	2835 psig	2835 psig
Secondary Pressure	0 psig	0 psig
Primary Fluid Temperature (T_{hot})	608.0°F	620.3°F
Secondary Fluid Temperature	519.8°F	538.8°F

(1) Low T_{ave} with 10% Tube Plugging case in Reference A-2.
(2) High T_{ave} with 0% Tube Plugging case in Reference A-2.

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.1.3 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.1.5 Summary of FEA Results for Tube-to-Tubesheet Contact Pressures

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

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 RPC 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 RPC justification in the sense of WCAP-16152 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 [

]a,c,e

The end cap loads for Normal and Faulted conditions are:

Normal (maximum):	$\pi * (2235-792) * (0.764)^2 /4 = 659.69$ lbs.
Faulted (FLB):	$\pi * 2835 * (0.764)^2 /4 = 1299.66$ lbs.
Faulted (SLB):	$\pi * 2560 * (0.764)^2 /4 = 1173.59$ 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 of the residual strength of the joint. The data from this series of pullout tests are listed in Table A.12. 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.) [

]^{a,c,e}

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

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

where: F_{HE} = Resistance to pull out due to the initial hydraulic expansion = 326.8 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.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. The above equation is also used to find the minimum contact lengths needed to meet the pullout force requirements. 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. The above equation is also used to find the minimum contact lengths needed to meet the pull out force requirements. This length is 8.61 inches for the 1.43 times the accident pressure performance criterion which corresponds to a pullout force of 1859 lbs in the Hot Leg for the Faulted (SLB) condition. The minimum contact length needed to meet the pullout force requirement of 1859 lb. for the Faulted (FLB) 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.13. The H* results for each zone are summarized in Table A.14.

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 8.61 inches in Zone C, Reference A-18.

A.3 References

A-1. WCAP-16152-P (Proprietary), "Justification for the Partial-Length Rotating Pancake Coil (RPC) Inspection of the Tube Joints of the Byron/Braidwood Unit 2 Model D5 Steam Generators," Westinghouse Electric Company LLC, Pittsburgh, PA, December 2003.

A-2. CN-SGDA-03-85 (Proprietary), Rev. 1, "H*/P* Input for Model D-5 and Model F Steam Generators," Westinghouse Electric Company LLC, Pittsburgh, PA, September 2003.

A-3. WCAP-15932-P (Proprietary), Rev. 1, "Improved Justification of Partial-Length RPC Inspection of Tube Joints of Model F Steam Generators of Ameren-UE Callaway Plant," Westinghouse Electric Company LLC, Pittsburgh, PA, May 2003.

A-4. NSD-E-SGDA-98-361 (Proprietary), "Transmittal of Yonggwang Unit 2 Nuclear Power Plant Steam Generator Tube-to-Tubesheet Joint Evaluation," Westinghouse Electric Company LLC, Pittsburgh, PA, November 1998.

A-5. STD-MCE-03-49 (Proprietary), "D5 Leak Rate Testing," Westinghouse Electric Company LLC, Pittsburgh, PA, November 2003.

A-6. DP-SGDA-03-1 (Proprietary Report), "Data Package for Strength Testing of Model D5 SG Tube Joint for H-Star for Comanche Peak 2, Byron 2/Braidwood 2 and Catawba 2," Westinghouse Electric Company LLC, Pittsburgh, PA, January 2003.

A-7. WCAP-11228 (Proprietary), Rev. 1, "Tubesheet Region Plugging Criterion for the South Carolina Electric and Gas Company V. C. Summer Nuclear Station Steam Generators," Westinghouse Electric Company LLC, Pittsburgh, PA, October 1986.

A-8. LTR-SGDA-03-122 (Proprietary), "Callaway Tubesheet Indications Database Activity," Westinghouse Electric Company LLC, Pittsburgh, PA, May 2003.

A-9. CN-SGDA-03-123 (Proprietary), "Leakage Calculations to Support H* Criterion for Model D5 Steam Generators," Westinghouse Electric Company LLC, Pittsburgh, PA, October 2003.

A-10. CN-SGDA-03-121 (Proprietary), "H* Ligament Tearing for Models F and D5 Steam Generators," Westinghouse Electric Company LLC, Pittsburgh, PA, October 2003.

A-11. ASME Boiler and Pressure Vessel Code Section III, "Rules for Construction of Nuclear Power Plant Components," 1989 Edition, The American Society of Mechanical Engineers, New York, NY

A-12. Porowski, J. S. and O'Donnell, W. J., "Elastic Design Methods for Perforated Plates," Transactions of the ASME Journal of Engineering for Power, Vol. 100, p. 356, 1978.

A-13. Slot, T., "Stress Analysis of Thick Perforated Plates," PhD Thesis, Technomic Publishing Co., Westport, CN, 1972.

A-14. Computer Program WECAN/Plus, "User's Manual", 2nd Edition, Revision D, Westinghouse Government Services LLC, Cheswick, Pennsylvania, May 1, 2000.

A-15. SM-98-102 (Proprietary), Rev. 2, "Tube/Tubesheet Contact Pressures for Yonggwang 2," Westinghouse Electric Company LLC, Pittsburgh, PA, November 1998.

A-16. Nelson, L. A., "Reference for Model D Tubesheet Simulant of 1.800 Inch Diameter", electronic mail (Contained in Reference A-18), Westinghouse Electric Company LLC, Pittsburgh, PA, August 6, 2003.

A-17. 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, March 2003.

A-18. CN-SGDA-03-87 (Proprietary), "Evaluation of the Tube/Tubesheet Contact Pressures and H* for Model D5 Steam Generators at Byron, Braidwood, Catawba and Comanche Peak," Westinghouse Electric Company LLC, Pittsburgh, PA, October 2003.

A-19. WNET-153, Vol. 5 (Proprietary), "Model D5 Steam Generator Stress Report: Tube Analysis," Westinghouse Electric Company LLC, Pittsburgh, PA, March, 1982.

A-20. CN-SGDA-03-133 (Proprietary), Rev. 0, "Evaluation of the H* Zone Boundaries for Specific Model D5 and Model F 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

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

Table 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 ($psi \cdot 10^6$)	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

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

a,c,e

[ ]

Table A.6: Tube/Tubesheet Maximum & Minimum Contact Pressures & H* for Byron/Braidwood Unit 2 Steam Generators

a,c,e

[ ]

Table A.7: Cumulative Forces Resisting Pull Out from the Top of the Tubesheet
Byron/Braidwood 2 – Hot Leg Normal Conditions
Low T_{ave}, High T_{sec}

a,c,e

Table A.8: Cumulative Forces Resisting Pull Out from the TTS
Byron/Braidwood 2 – Hot Leg Normal Conditions
High T_{ave}, Low T_{sec}

a,c,e

Table A.9: Cumulative Forces Resisting Pull Out from the TTS Byron/Braidwood 2–Faulted (SLB) Conditions

a,c,e

Table A.10: Cumulative Forces Resisting Pull Out from the TTS Byron/Braidwood 2–FLB Conditions Low T_{ave}, High T_{sec}

a,c,e

[ ]

Table A.11: Cumulative Forces Resisting Pull Out for FLB Conditions
High T_{ave}, Low T_{sec}

a,c,e

Table A.12: Large Displacement, 0.25 in. Pullout Test Data

a,c,e

[redacted]

Table A.13: Summary of H* Calculations for Byron/Braidwood Unit 2

a,c,e

[redacted]

Table A.14: H* Summary Table

Zone	Limiting Loading Condition	Engagement from TTS (inches)
A	3.0 NO ΔP (1,2)	2.95 (3)
B	1.43 SLB ΔP (1,2)	6.00
C	1.43 SLB ΔP (1,2)	8.61

Notes:

1. Seismic loads have been considered and are not significant in the tube joint region (Reference A-19 8.17).
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 from Table 7.2-5 to account for the hydraulic expansion transition region at the top of the tubesheet.

a,c,e

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

a,c,e

Figure A.2: Contact Pressures for Normal Condition (T_{min}) at Byron/Braidwood 2

a,c,e

Figure A.3: Contact Pressures for Normal Condition (T_{max}) at Byron and Braidwood Unit 2

a,c,e

Figure A.4: Contact Pressures for SLB Faulted Condition at Byron and Braidwood 2

a,c,e

Figure A.5: Contact Pressures for FLB Faulted Condition at Byron and Braidwood 2 (T_{min})

a,c,e

Figure A.6: Contact Pressures for FLB Faulted Condition at Byron and Braidwood 2 (T_{max})

a,c,e

Figure A.7: D5 Pullout Test Results for Force/inch at 0.25 inch Displacement