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TUBESHEET REGION PLUGGING CRITERION
FOR THE SOUTH CAROLINA ELECTRIC
AND GAS COMPANY V. C. SUMMER
NUCLEAR STATION STEAM GENERATORS

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ABSTRACT

An evaluation was performed to develop a plugging criterion, known as the F* criterion, for determining whether or not repairing or plugging of full depth hardroll expanded steam generator tubes is necessary for degradation that has been detected in the region of the tube located within the tubesheet. The evaluation consisted of analysis and testing programs aimed at quantifying the residual radial preload of Westinghouse Model D steam generator tubes hardrolled into the tubesheet. Analysis was performed to determine the length of hardroll engagement required to resist tube pullout forces during normal and faulted plant operation. The analytically determined values were verified as conservative by both pullout and proof testing. It was postulated that the radial preload would be sufficient to significantly restrict leakage during normal and operating conditions. This was also verified by the proof tests which exhibited no leakage under simulated operating mechanical conditions. On this basis an F* criterion value of [ ]^{a,c,e} inches was established as sufficient for continued plant operation regardless of the extent of tube degradation below F*. The evaluation also demonstrates that application of the F* criterion for tube degradation within the tubesheet affords a level of plant protection commensurate with that provided by RG 1.121 for degradation located outside of the tubesheet region.

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TUBESHEET REGION PLUGGING CRITERION FOR FULL DEPTH HARDROLL EXPANDED TUBES

1.0 INTRODUCTION

The purpose of this report is to document the development of a criterion to be used in determining whether or not repairing or plugging of full depth hardroll expanded steam generator tubes is necessary for degradation which has been detected in that portion of the tube which is within the tubesheet. Existing South Carolina Electric & Gas V. C. Summer Nuclear Station plant Technical Specification tube repairing/plugging criteria apply throughout the tube length, but, do not take into account the reinforcing effect of the tubesheet on the external surface of the tube. The presence of the tubesheet will constrain the tube and will complement its integrity in that region by essentially precluding tube deformation beyond its expanded outside diameter. The resistance to both tube rupture and tube collapse is significantly strengthened by the tubesheet. In addition, the proximity of the tubesheet significantly affects the leak behavior of through wall tube cracks in this region, i.e., no significant leakage relative to plant technical specification allowables is to be expected. Based on these considerations, the use of an alternate criterion for establishing plugging margin is justified.

This evaluation forms the basis for the development of a criterion for obviating the need to repair a tube (by sleeving) or to remove a tube from service (by plugging) due to detection of indications, e.g., by eddy current testing (ECT), in a region extending over most of the length of tubing within the tubesheet. This evaluation applies to the V. C. Summer Nuclear Station Westinghouse Model D3 steam generators and assesses the integrity of the tube bundle, for tube ECT indications occurring on the length of tubing within the tubesheet, relative to:

1) Maintenance of tube integrity for all loadings associated with normal plant conditions, including startup, operation in power range, hot standby and cooldown, as well as all anticipated transients.

2) Maintenance of tube integrity under postulated limiting conditions of primary to secondary and secondary to primary differential pressure, e.g., steamline break (SLB).

3) Limitation of primary to secondary leakage consistent with accident analysis assumptions.

The result of the evaluation is the identification of a distance, designated F* (and identified as the F* criterion), below the top of the tubesheet for which tube degradation of any extent does not necessitate remedial action, e.g., plugging or sleeving. The F* criterion provides for sufficient engagement of the tube to tubesheet hardroll such that pullout forces that could be developed during normal or accident operating conditions would be successfully resisted by the elastic preload between the tube and tubesheet. The necessary engagement length applicable to the V. C. Summer plant steam generators was found to be [ ]a,c,e, inches based on preload analysis. Verification that this value is significantly conservative was demonstrated by both pullout and hydraulic proof testing of tubes in tubesheet simulating collars. Application of the F* criterion provides a level of protection for tube degradation in the tubesheet region commensurate with that afforded by Regulatory Guide (RG) 1.121, reference 1, for degradation located outside the tubesheet region.

2.0 EVALUATION

Tube rupture in the conventional sense, i.e., characterized by an axially oriented "fishmouth" opening in the side of the tube, is not possible within the tubesheet. The reason for this is that the tubesheet material prevents the wall of the tube from expanding outward in response to the internally acting pressure forces. The forces which would normally act to cause crack extension are transmitted into the walls of the tubesheet, the same as for a nondegraded tube, instead of acting on the tube material. Thus, axially oriented linear indications, e.g., cracks, cannot lead to tube failure within the tubesheet and may be considered on the basis of leakage effects only.

Likewise, a circumferentially oriented tube rupture is resisted because the tube is not free to deform in bending within the tubesheet. When degradation has occurred such that the remaining tube cross sectional area does not present a uniform resistance to axial loading, bending stresses are developed which may significantly accelerate failure. When bending forces are resisted by lateral support loads, provided by the tubesheet, the acceleration mechanism is mitigated and a tube separation mode similar to that which would occur in a tensile test results. Such a separation mode, however, requires the application of significantly higher loads than for the unsupported case.

In order to evaluate the applicability of any developed criterion for indications within the tubesheet some postulated type of degradation must necessarily be considered. For this evaluation it was postulated that a circumferential severance of a tube could occur, contrary to existing plant operating experience. However, implicit in assuming a circumferential severance to occur, is the consideration that degradation of any extent could be demonstrated to be tolerable below the location determined acceptable for the postulated condition.

When the tubes have been hardrolled into the tubesheet, any axial loads developed by pressure and/or mechanical forces acting on the tubes are resisted by frictional forces developed by the elastic preload that exists between the tube and the tubesheet. For some specific length of engagement of the hardroll, no significant axial forces will be transmitted further along the tube, and that length of tubing, i.e., F*, will be sufficient to anchor the tube in the tubesheet. In order to determine the value of F* for application in Model D steam generators a testing program was conducted to measure the elastic preload of the tubes in the tubesheet.

The presence of the elastic preload also presents a significant resistance to flow of primary to secondary or secondary to primary water for degradation which has progressed fully through the thickness of the tube. In effect, no leakage would be expected if a sufficient length of hardroll is present. This has been demonstrated in high pressure fossil boilers where hardrolling of tube to the tubesheet joints is the only mechanism resisting flow, and in steam generator sleeve to tube joints made by the Westinghouse hybrid expansion joint (HEJ) process.

2.1 DETERMINATION OF ELASTIC PRELOAD BETWEEN THE TUBE AND TUBESHEET

Tubes are installed in the steam generator tubesheet by a hardrolling process which expands the tube to bring the outside surface into intimate contact with the tubesheet hole. The roll process and roll torque are specified to result in a metal to metal interference fit between the tube and the tubesheet.

A test program was conducted by Westinghouse to quantify the degree of interference fit between the tube and the tubesheet provided by the full depth mechanical hardrolling operation. The data generated in these tests has been analyzed to determine the length of hardroll required to preclude axial tube forces from being transmitted further along the tube, i.e., to establish the F* criterion. The amount of interference was determined by installing tube specimens in collars specifically designed to simulate the tubesheet radial stiffness. A hardroll process representative of that used during steam generator manufacture was used in order to obtain specimens which would exhibit installed preload characteristics like the tubes in the tubesheet.

Once the hardrolling was completed, the test collars were removed from the tube specimens and the springback of the tube was measured. The amount of springback was used in an analysis to determine the magnitude of the interference fit, which is, therefore, representative of the residual tube to tubesheet radial load in Westinghouse Model D steam generators.

2.1.1 RADIAL PRELOAD TEST CONFIGURATION DESCRIPTION

The test program was designed to simulate the interface of a tube to tubesheet full depth hardroll for a model D steam generator. The test configuration consisted of six cylindrical collars, approximately [ ]^{a,c,e} inches in length, [ ]^{a,c,e} inches in outside diameter, and [ ]^{a,c,e} inch in inside diameter. A mill annealed, Inconel 600 (ASME SB-163), tubing specimen, approximately [ ]^{a,c,e} inches long with a nominal [ ]^{a,c,e} outside diameter before rolling, was hard rolled into each collar using a process which simulated actual tube installation conditions.

THe design of the collars was based on the results of performing finite element analysis of a section of the steam generator tubesheet to determine radial stiffness and flexibility. The inside diameter of the collar was chosen to match the size of holes drilled in the tubesheet. The outside diameter was selected to result in the same radial stiffness as the tubesheet.

The collars were fabricated from AISI 1010 carbon steel similar in mechanical properties to the actual tubesheet material. The collar assembly was clamped in a vise during the rolling process and for the post roll measurements of the tube ID. Following the recording of all post roll measurements, the collars were saw cut to within a small distance from the tube wall. The collars were then split for removal from the tube and tube ID and OD measurements repeated. In addition, the axial length of the tube within the collar was measured both before and after collar removal.

Two end boundary conditions were imposed on the tube specimen during rolling. The end was restrained from axial motion in order to perform a tack roll at the bottom end, and was allowed to expand freely during the final roll.

2.1.2 PRELOAD TEST RESULTS DISCUSSION AND ANALYSIS

All measurements taken during the test program are tabulated in Table 1. The data recorded was that necessary to determine the interfacial conditions of the tubes and collars. These consisted of the ID and OD of the tubes prior to and after rolling and removal from the collars as well as the inside and outside dimensions of each collar before and after tube rolling. Two orthogonal measurements were taken at each of six axial locations within the collars and tubes. In addition, gage marks were put on the tubes so that any axial deformation that occurred during collar removal might be monitored. All measured dimensions given in Table 1. are in inch units. The remainder of the data of particular interest was calculated from these specific dimensions. The calculated dimensions included wall thickness, change in wall thickness for both rolling and removal of the tubes from the collars, and percent of spring back. It is to be noted that location number 1 of the test data was in the roll transition area. Reproducibility of the measurements was not representative of the actual hardroll region and the data for this location were not included in the calculations for averages of deflection and stress.

Using the measured and calculated physical dimensions, an analysis of the tube deflections was performed to determine the amount of preload radial stress present following the hardrolling. The analysis consisted of application of conventional thick tube equations to account for variation of structural parameters through the wall thickness. However, traditional application of cylinder analysis considers the tube to be in a state of plane stress. For these tests the results implied that the tubes were in a state of plane strain elastically. This is in agreement with historical findings that theoretical values for radial residual preload are below those actually measured, and that axial frictional stress between the tube and the tubesheet increases the residual pressure. In a plane stress analysis such stress is taken to be zero, references 2 and 3. Based on this information the classical equations relating tube deformation and stress to applied pressure were modified to reflect plane strain assumptions.

The standard analysis of thick walled cylinders results in an equation for the radial deflection of the tube as:

$$u = C_1 * r + C_2 / r \qquad (1)$$

where, u = radial deflection
r = radial position within the tube wall,

and the constants, C_1 and C_2 are found from the boundary conditions to be functions of the elastic modulus of the material, Poisson's ratio for the material, the inside and outside radii, and the applied internal and external pressures. The difference between an analysis assuming plane stress and one assuming plane strain is manifested only in a change in the constant C_2. The first constant is the same for both conditions. For materials having a Poisson's ratio of 0.3, the following relation holds for the second constant:

$$C_2(\text{Plane Strain}) = 0.862 * C_2(\text{Plane Stress}) \qquad (2)$$

The effect on the calculated residual pressure is that plane strain results are higher than plane stress results by slightly less than 10 percent. Comparing this effect with the results reported in reference 2 indicated that better agreement with test values is achieved. It is to be noted that the residual radial pressure at the tube to tubesheet interface is the compressive radial stress at the OD of the tube.

By substituting the expressions for the constants into equation (1) the deflection at any radial location within the tube wall as a function of the internal and external pressure (radial stress at the ID and OD) is found. This expression was differentiated to obtain flexibility values for the tube deflection at the ID and OD respectively, e.g., dUi/dPo is the ratio of the radial deflection at the ID due to an OD pressure. Thus, dUi/dPo was used to find the interface pressure and radial stress between the tube and the tubesheet as:

$$S_{ro} = - P_o = - (\text{ID Radial Springback}) / (dUi/dPo) \qquad (3)$$

The calculated radial residual stress for each specimen at each location is tabulated in Table 2. Using all of the data, except location 1 for each specimen, and location 6 for specimen 2 (which was judged to be an outlier as it is more than three standard deviations from the mean of the data), the mean residual radial stress and the standard deviation was found to be [ ]^{a,c,e} psi and [ ]^{a,c,e} psi respectively. In order to determine a value to be used in the analysis, a tolerance factor for [ ]^{a,c,e} percent confidence to contain [ ]^{a,c,e} percent of the population was calculated, considering the [ ]^{a,c,e} useable data points, to be [ ]^{a,c,e}. Thus, a [ ]^{a,c,e} lower tolerance limit (LTL) for the radial residual preload at room temperature is [ ]^{a,c,e} psi.

2.1.3 RESIDUAL RADIAL PRELOAD DURING PLANT OPERATION

During plant operation the amount of preload will change depending on the pressure and temperature conditions experienced by the tube. The room temperature preload stresses, i.e., radial, circumferential and axial, are such that the material is nearly in the yield state if a comparison is made to ASME Code, reference 4, minimum material properties. Since the coefficient of thermal expansion of the tube is greater than that of the tubesheet, heatup of the plant will result in an increase in the preload and could result in some yielding of the tube. In addition, the yield strength of the tube material decreases with temperature. Both of these effects may result in the preload being reduced upon return to ambient temperature conditions, i.e., in the cold condition. Based on the results obtained from the pullout tests, reported in section 2.3.2, this is not expected to be the case as even with a very high thermal relaxation soak the results show the analysis to be conservative.

The plant operating pressure influences the preload directly based on the application of the pressure load to the ID of the tube, thus increasing the amount of interface loading. The pressure also acts indirectly to decrease the amount of interface loading by causing the tubesheet to bow upward. This bow results in a dilatation of the tubesheet holes, thus, reducing the amount of tube to tubesheet preload. Each of these effects may be quantitatively treated.

The maximum amount of tubesheet bow loss of preload will occur at the top of the tubesheet. Since F* is measured from the bottom of the hardroll transition (BRT) or the top of the tubesheet, and leakage is to be restricted by the portion of the tube above F*, the potential for the tube section above F* to experience a net loosening during operation is considered for evaluation. The effects of the three identified mechanisms affecting the preload are considered as follows:

1. Thermal Expansion Tightening - The mean coefficient of thermal expansion for the Inconel tubing between ambient conditions and $600^{o}F$ is $7.80*10^{-6}$ in/in/^{o}F. That for the steam generator tubesheet is $7.28*10^{-6}$ in/in/^{o}F. Thus, there is a net difference of $0.52*10^{-6}$ in/in/^{o}F in the expansion property of the two materials. Considering a temperature difference of $550^{o}F$ between ambient and operating conditions the increase in preload between the tube and the tubesheet was calculated as:

   $$S_{rT} = (0.52E\text{-}6)*(550)*(\text{Collar ID}) / 2 / (dUo/dPo) \quad (4)$$

   This calculation was also performed and tabulated in Table 2. The results indicate that the increase in preload radial stress due to thermal expansion is [ ]^{a,c,e} psi. It is to be noted that this value applies for both normal operating and faulted conditions.

2. Internal Pressure Tightening - The maximum normal operating differential pressure from the primary to secondary side of the steam generator is [ ]a,c,e psi during a loss of load transient. The internal pressure acting on the wall of the tube will result in an increase of the radial preload on the order of the pressure value. The increase was found as:

$$S_{rP} = - Po = - P_i * (dUo/dPi) / (dUo/dPo) \qquad (5)$$

In actuality, the increase in preload will be more dependent on the internal pressure of the tube since water at secondary side pressure would not be expected between the tube and the tubesheet.

Results from the performance of this calculation are tabulated on Table 2. for normal operating conditions and summarized on the summary sheet for both normal and faulted conditions. The results indicate that the increase in preload radial stress is [ ]a,c,e psi for normal operating conditions and [ ]a,c,e psi for faulted (FLB) operating conditions.

3. Tubesheet Bow Loosening - An analysis of the Model D3 tubesheet was performed to evaluate the loss of preload stress that would occur as a result of tubesheet bow. The analysis was based on performing finite element analysis of the tubesheet and SG shell using equivalent perforated plate properties for the tubesheet, reference 3. Boundary conditions from the results were then applied to a smaller, but more detailed model, in order to obtain results for the tubesheet holes. Basically the deflection of the tubesheet was used to find the stresses active on the top surface and then the presence of the holes was accounted for. For the location where the loss of preload is a maximum, the radial preload stress would be reduced by [ ]a,c,e psi during normal operation and [ ]a,c,e psi during faulted (SLB) operating conditions. During LOCA the differential operating pressure is from secondary to primary. Thus, the radial preload will increase by [ ]a,c,e psi as the tubesheet bows downward .

Combining the room temperature hardroll preload with the thermal and pressure effects results in a net operating preload of [ ]a,c,e psi during normal operation and [ ]a,c,e psi for faulted operation. In addition to restraining the tube in the tubesheet, this preload should effectively retard leakage from indications in the tubesheet region of the tubes.

2.2 ENGAGEMENT DISTANCE DETERMINATION

The calculation of the value of F* recommended for application to the V. C. Summer Units' steam generators is based on determining the length of hardroll necessary to equilibrate the applied loads during the maximum normal operating conditions or faulted conditions, whichever provides the least value. Thus, the applied loads are equilibrated to the load carrying ability of the hardrolled tube for both of the above conditions. In performing the analysis, consideration is made of the potential for the ends of the hardroll at the hardroll transition and the assumed severed condition to have a reduced load carrying capability.

2.2.1 APPLIED LOADS

The applied loads to the tubes which could result in pullout from the tubesheet during all normal and postulated accident conditions are predominantly axial and due to the internal to external pressure differences. For a tube which has not been degraded, the axial pressure load is given by the product of the pressure with the internal cross-sectional area. However, for a tube with internal degradation, e.g., cracks oriented at an angle to the axis of the tube, the internal pressure may also act on the flanks of the degradation. Thus, for a tube which is conservatively postulated to be severed at some location within the tubesheet, the total force acting to remove the tube from the tubesheet is given by the product of the pressure and the cross-sectional area of the tubesheet hole. The force resulting from the pressure and internal area acts to pull the tube from the tubesheet and the force acting on the end of the tube tends to push the tube from the tubesheet. For this analysis, the tubesheet hole diameter has been used to determine the magnitude of the pressure forces acting on the tube. The forces acting to remove the tube from the tubesheet are [ ]^{a,c,e} pounds and [ ]^{a,c,e} pounds respectively for normal and faulted operating conditions. Any other forces such as fluid drag forces in the U-bends and vertical seismic forces are negligible by comparison.

2.2.2 END EFFECTS

The analysis for the radial preload pressure between the tube and the tubesheet made no consideration of the effect of the material discontinuity at the hardroll transition to the unexpanded length of tubing. In addition, for a tube which is postulated to be severed within the tubesheet there is a material discontinuity at the location where the tube is severed. For a small distance from each discontinuity the stiffness, and hence the radial preload, of the tube is reduced relative to that remote from the ends. The analysis of end effects in thin cylinders is based on the analysis of a beam on an elastic foundation. For a tube with a given radial deflection at the end, the deflection of points away from the end relative to the end deflection is given by:

$$u_{rx} / u_{ro} = e^{-kx} * \text{cosine} (k * x) \qquad (6)$$

where, k = [ ]^{a,c,e} for Model D roll expanded tubes.
x = Distance from the end of the tube.

For the radially preloaded tube, the distance for the end effects to become negligible is the location where the cosine term becomes zero. Thus for the roll expanded Model D tubes the distance corresponds to the product of "k" times "x" being equal to (pi/2) or [ ]^{a,c,e} inch.

The above equation can be integrated to find the average deflection over the affected length to be 0.384 of the end deflection. This means that on the average the stiffness of the material over the affected length is 0.616 of the stiffness of the material remote from the ends. Therefore, the effective preload for the affected end lengths is 61.6 percent of the preload at regions more than [ ]^{a,c,e} inch from the ends. For example, for the normal operating net preload of [ ]^{a,c,e} psi or [ ]^{a,c,e} pounds per inch of length, the effective preload for a distance of [ ]^{a,c,e} inch from the end is [ ]^{a,c,e} pounds per inch or [ ]^{a,c,e} pounds.

2.2.3 CALCULATION OF ENGAGEMENT DISTANCE REQUIRED, F*

The calculation of the required engagement distance is based on determining the length for preload frictional forces to equilibrate the applied operating loads. The axial friction force was found as the product of the radial preload force and the coefficient of friction between the tube and the tubesheet. The value assumed for the coefficient of friction was [ ]^{a,c,e}, reference 5 for sliding of nickel on mild steel under "greasy" conditions. For normal operation the radial preload is [ ]^{a,c,e} psi or [ ]^{a,c,e} pounds per inch of engagement. Thus, the axial friction resistance force is [ ]^{a,c,e} pounds per inch of engagement. It is to be noted that this value applies away from the ends of the tube. For any given engagement length, the total axial resistance is the sum of that provided by the two ends plus that provided by the length minus the two end lengths. From the preceding section the axial resistance of each end is [ ]^{a,c,e} pounds. Considering both ends of the presumed severed tube, i.e., the hardroll transition is considered one end, the axial resistance is [ ]^{a,c,e} pounds plus the resistance of the material between the ends, i.e., the total length of engagement minus [ ]^{a,c,e} inch. For example, a one inch length has an axial resistance of,

[ ]^{a,c,e}

Conversely, for the maximum normal pressure applied load of [ ]^{a,c,e} pounds, considered as [ ]^{a,c,e} pounds with a safety factor of 3, the length of hardroll required is given by,

F* = [ ]^{a,c,e}

Similarly, the required engagement length for faulted conditions can be found to be [ ]^{a,c,e} inch using a safety factor of 1.43 (corresponding to a ASME Code safety factor of 1.0/0.7 for allowable stress for faulted conditions).

The calculation of the above values is summarized in Table 3. The F* value thus determined for the required length of hardroll engagement below the BRT or the top of the tubesheet, whichever is greater relative to the top of the tubesheet, for normal operation is sufficient to resist tube pullout during both normal and postulated accident condition loadings.

Based on the results of the testing and analysis, it is concluded that following the installation of a tube by the standard hardrolling process, a residual radial preload stress exists due to the plastic deformation of the tube and tubesheet interface. This residual stress is expected to restrain the tube in the tubesheet while providing a leak limiting seal condition.

2.2.4 OTHER TRANSIENT CONSIDERATIONS

An evaluation was performed to consider operating transients which could result in the condition where the tube would be at a temperature lower than the tubesheet. In this situation some of the engagement preload would be lost as the tube would shrink relative to the tubesheet. The worst case occurs for a Reactor Trip from Full Power where the tube temperature becomes about [ ]^{a,c,e} degrees lower than the tubesheet temperature. This temperature difference will result in a loss of preload of about [ ]^{a,c,e} percent of the LTL used in the analysis. However, the transient starts from a full power condition where the differential pressure, [ ]^{a,c,e} psi, is about [ ]^{a,c,e} percent lower than the maximum differential pressure used in the analysis performed to determine the required length of engagement. Thus, the applied pressure load decreases relatively more than the tube to tubesheet preload and the margin of safety is not reduced.

2.2.5 OTHER FAULTED LOADS CONSIDERATION

The differential pressure acting across the Flow Distribution Baffle (FDB) during a FLB would be expected to cause an out-of-plane rotation of the FDB. If the pressure loading is high enough, the FDB rotation will result in tube contact and the generation of axial loads on the tubes. A nonlinear, elastic plastic finite element analysis, reference 6., using the computer code WECAN, reference 7., was performed to determine the magnitude of the tube axial loads due to interaction of the FDB with the tubes during a FDB.

The finite element model used for the analysis considered the FDB as an equivalent solid plate using three dimensional plastic shell elements. The equivalent material properties for the plate were calculated on the basis of nominal tube hole and pitch dimensions. However, in calculating the plate deflection to result in initial plate to tube contact the minimum tube to plate clearance dimensions were used. Tube stiffnesses were incorporated into the solution when plate rotation was determined to be at a level which would result in tube contact. The model also considered the stayrod spacer pipes as flexible supports, while the back-up bars on the on the boundary were assumed to act as rigid supports with out of plane restraint only. No plate restraint was considered to be offered by the wedges.

The maximum plate rotation and axial tube loads were found to occur near the center of the baffle plate. The analysis was also performed considering a reduced free rotation of the plate prior to contact and loading of a tube in order to consider the results of postulated tube denting. The maximum axial tube loading was obtained utilizing the pressure differential for the highest loaded tube support plate located anywhere in the preheater.

For the cases considered the maximum axial loading on the tubes was found to be insignificant relative to the axial pressure loads.

Seismic analysis of Model D steam generators, reference 8, has likewise shown that axial loading of the tubes is negligible during a safe shutdown earthquake (SSE).

2.3 ROLLED TUBE PULLOUT TESTS

The engagement distance determination discussed in Section 2.2.3 was calculated from a derived preload force and an assumed static coefficient of friction for tube to tubesheet contact. A direct measurement of this static coefficient of friction is difficult. However, a simple pull test on a rolled tube joint provided both support for the derived preload force (less the effects of thermal expansion and internal pressure tightening) as well as an indirect measurement of the static coefficient of friction. The results of the testing verify the calculation as being conservative. An estimate of the static coefficient of friction was calculated using the end effect adjustment described in Section 2.2.2.

2.3.1 PULLOUT TEST CONFIGURATION DESCRIPTION

Pressure tests were conducted on rolled joints of [ ]^{a,c,e} inches in length and with nominal degrees of wall thinning of [ ]^{a,c,e}. Wall thinning at the [ ]^{a,c,e} levels were difficult to control and the actual wall thinning as measured represents the best achievable. As with the preload tests, the test configuration consisted of mill annealed, Inconel 600 (ASME SB-163) Model D3 tubing, hard rolled into carbon steel collars with an outside diameter to simulate tubesheet rigidity. Inside surface roughness values of the collars were measured and recorded. The specification of surface roughness for the fabrication of the collars was the same as that used for the fabrication of the model D tubesheets. Prior to rolling, the tubing was tack rolled and welded to the collar similar to the installation of tubes in the steam generators. The hard rolling was done in a direction away from the weld and in all aspects simulated actual tube installation conditions. After rolling, an inside circumferential cut was machined through the wall of the tube at a controlled distance from the bottom of the hardroll transition (opposite the tube weld). The machined cut simulated a severed tube condition. To simulate any possible effect of reduced preload force due to tube yielding during manufacturing heat treatment and during reactor operation, the samples were subjected to a heat soak of [ ]^{a,c,e}. The pullout tests were performed on a tensile testing machine, in air at room temperature using a crosshead travel rate of [ ]^{a,c,e}. Thus, for the tests there is no increase in preload due to thermal expansion of the tube relative to the collar.

2.3.2 PULLOUT TEST RESULTS DISCUSSION AND ANALYSIS

The results of these tests are tabulated in Table 4. During the pull, the tube typically showed some small load relaxation and recovery prior to achieving the maximum pullout value. This is probably due to slippage on a microscopic scale at the interface in order to further distribute the load along the length of the interface. It is thought that some initial small movement within the joint was necessary to develop the maximum contact and resistance to pullout. This was not directly observed, and would be difficult as the axial loads required were on a scale which could cause yielding of the tube in the axial direction. For a rolled joint of [ ]^{a,c,e} inch length with nominal wall thinning, the maximum pullout force was typically [ ]^{a,c,e} lbs, corresponding to an axial stress of [ ]^{a,c,e} psi. Based on the previously derived nominal preload stress due to hardrolling of [ ]^{a,c,e} psi, the implied maximum coefficient of friction (f) would be:

[

]^{a,c,e}

The [ ]^{a,c,e} factor represents the reduction in effective length due to the loss of rigidity at the ends (end effect). Based on the observed pullout forces, the coefficient of friction assumed previously ([ ]^{a,c,e}) is conservative by a factor of [ ]^{a,c,e} relative to a dry interface between the tube and collar.

2.4 ROLLED TUBE HYDRAULIC PROOF TESTS

The pullout tests discussed in the previous section provided support for the derived preload force (less the effects of thermal expansion and pressure tightening) and provided an indirect measurement of the static coefficient of friction between the tube and the tubesheet. Similar tests were conducted that used internal pressure as the acting force on the tube. While the thermal expansion tightening and the tubesheet bow loosening effects would not be represented by the this test, it would include the other factors such as preload force due to rolling, internal pressure tightening, tube-to-tubesheet coefficient of friction, tube end effects, and leakage propensity. Thermal expansion tightening and tubesheet bow loosening, being approximately the same magnitude under normal operating conditions, would offset each other. Therefore, by using internal pressure as the acting force, the rolled joint mechanics would be most like the postulated FLB or steamline break (SLB) conditions and would thereby represent a direct verification of the conservative nature of the calculated required engagement distance.

2.4.1 PROOF TEST CONFIGURATION DISCUSSION

Similar to the rolled tube pullout tests, pressure tests were conducted on rolled joints of [ ]^{a,c,e} in length and with nominal degrees of wall thinning of [ ]a,c,e. As with the preload and pullout tests, the test configuration consisted of mill annealed, Inconel 600 (ASME SB-163) Model D3 tubing, hard rolled into carbon steel collars with an outside diameter to simulate tubesheet rigidity. As with the pullout test samples, a machined cut was used to simulate a severed tube condition. To simulate any possible effects of reduced preload force due to tube yielding during manufacturing heat treatment, these samples were also subjected to a heat soak of [ ]a,c,e. The pressure tests were performed at room temperature using DI water at a pressurizing rate of approximately [ ]a,c,e.

2.4.2 PROOF TEST RESULTS DISCUSSION AND ANALYSIS

The results of these tests are tabulated in Table 5. The free span length of tubing outside of the collars was reinforced with external sleeves (using 7/8" tubing) after it was discovered that the retention forces were greater than those required to burst the tubes. Even with external sleeves, most of the tests resulted in the tubing bursting near the collar or near the fittings used to pressurize the samples.

No tubes with rolled joints of greater than [ ]a,c,e were expelled from the collars despite some samples being subjected to pressures as high as [ ]a,c,e psi. For the [ ]a,c,e engagement length tubes that were expelled, a clear absence of galling was evident. This indicates that the tube did not release primarily due to axial forces overcoming the tube to tubesheet friction for the length of the release, but possibly due to loss of pressure tightening caused by water ultimately being forced between the tube and the collar. Rationale supporting this postulated mechanism of release is based on the observation that the tubes did not slowly release from the collar, i.e., overcoming friction and galling as in the pull tests, rather for the few tubes that were expelled, the event was sudden.

Since leakage may represent a loss of internal pressure tightening, tests that ended with the rolled joint leaking may be considered as approaching the expulsion load. Throughout the tests, no leakage was observed other than when the tests were terminated due to leakage.

Considerations of force equilibrium indicate that there is a length of joint at which, for a given friction coefficient, the tube cannot be expelled from the collar regardless of any pressure increase. Data from the proof tests indicates that joints of length 1.0" or greater, with friction coefficients exhibited by the tested samples, may be incapable of being expelled at any pressure. In any event, the data from the tests showed that the rolled joint was stronger than the tubes. In fact, even for the [ ]a,c,e inch specimens, the joint failure load was about equal to the tube failure load for most of the tests.

These proof tests show that even for rolled joints of [ ]a,c,e in length at less than nominal wall thinning, pressure induced axial forces of several thousands of pounds or greater are necessary to cause the tube to release from the tubesheet. Thus, the preload based calculation of required engagement distance is indicated to be conservative.

2.5 LIMITATION OF PRIMARY-TO-SECONDARY LEAKAGE

The allowable amount of primary to secondary leakage in a steam generator during normal plant operation is limited by plant technical specifications, generally to 0.35 gpm. This limit, based on plant radiological release considerations and implicitly enveloping the leak before break consideration for a through wall crack in the free span of a tube, is also applicable to a leak source within the tubesheet. In evaluating the primary to secondary leakage aspect of the F* criterion, the relationship between the tubesheet region leak rate at postulated FLB or steamline break (SLB) conditions is assessed relative to that at normal plant operating conditions. The analysis was performed by assuming the existence of a leak path, however, no actual leak path would be expected due to the hardrolling of the tubes into the tubesheet. No leakage from any of the hydraulic proof test specimens occurred for pressures up to and in excess of faulted operating conditions.

2.5.1 OPERATING CONDITION LEAK CONSIDERATIONS

In actuality, as the test results substantiate for as little as [ ]^{a,c,e} inch of hardroll engagement, the hardrolled joint would be expected to be leak tight, i.e., the plant would not be expected to experience leak sources emanating below F*. Since the presence of the tubesheet tube indications is not expected to increase the likelihood that the plant would experience a significant number of leaks, it could also be expected, that if a primary to secondary leak is detected in a steam generator it is not in the tube region below F*. Thus, no significant radiation exposure due to the need for personnel to look for tube tubesheet leaks should be anticipated, i.e., the use of the F* criterion is consistent with ALARA considerations. As an additional benefit relative to ALARA considerations, precluding the need to install plugs below the F* criterion would result in a significant reduction of unnecessary radiation exposure to installing personnel.

The issue of leakage within the F* region up to the top of the mechanical roll transition (RT) assuming the as manufactured position of the roll transition is below the secondary side of the tubesheet includes the consideration of postulated accident conditions in which the violation of the tube wall is very extensive, i.e., that no material is required at all below F*. Based on operating plant and laboratory experience the expected configuration of any cracks, should they occur, is axial. The existence of significant circumferential cracking is considered to be of very low probability. Thus, consideration of whether or not a plant will come off-line to search for leaks a significant number of times should be based on the type of degradation that might be expected to occur, i.e., axial cracks. Axial cracks have been found both in plant operation and in laboratory experiments to be short, about 0.5 inch in length, and tight. In addition, for both the field and laboratory experience, once the cracks have grown so that the crack front is out of the skiproll or transition areas, they arrest.

Axial cracks in the free span portion of the tube, with no superimposed thinning, would leak at rates compatible with the technical specification acceptable leak rate. For a crack within the F* region of the tubesheet, expected leakage would be significantly less. Leakage through cracks in tubes has been investigated experimentally within Westinghouse for a significant number of tube wall thicknesses and thinning lengths, reference 8. In general, the amount of leakage through a crack for a particular size tube has been found to be approximately proportional to the fourth power of the crack length. Analyses have also been performed which show, on an approximate basis for both elastic and elastic-plastic crack behavior, that the expected dependency of the crack opening area for an unrestrained tube is on the order of the fourth power, e.g., see NUREG CR-3464. The amount of leakage through a crack will be proportional to the area of the opening, thus, the analytic results substantiate the test results.

The presence of the tubesheet will preclude deformation of the tube wall adjacent to the crack, i.e., the crack flanks, and the crack opening area may be considered to be directly proportional to the length. The additional dependency, i.e., fourth power relative to first power, is due to the dilatation of the unconstrained tube in the vicinity of the crack and the bending of the side faces or flanks of the crack. For a tube crack located within the tubesheet, the dilatation of the tube and bending of the side faces of the crack are suppressed. Thus, a 0.5 inch crack located within the F* region up to the top of the roll transition would be expected to leak, without considering the flow path between the tube and tubesheet, at a rate less than a similar crack in the free span, i.e., less than the V. C. Summer units technical specification limit of 0.35 gpm. Leakage would be expected to be about equal to that from a 0.0625 inch free span crack. Additional resistance provided by the tube to tubesheet annulus would reduce this amount even further, and in the hardroll region the residual radial preload would be expected to eliminate it. This conclusion is supported by the results of the preload testing and analysis, which demonstrated that a residual radial preload of about [ ][a,c,e] psi exists between the tube and the tubesheet at normal operating conditions. The conclusion was further supported by the hydraulic proof testing which showed no leakage for any of the joints tested at pressures significantly exceeding normal operating conditions.

2.5.2 POSTULATED ACCIDENT CONDITION LEAK CONSIDERATIONS

For the postulated leak source within the tubesheet, increasing the tube differential pressure increases the driving head for the leak and increases the tube to tubesheet loading. For an initial location of a leak source below the top of the tubesheet equal to F*, and without considering hardroll effects, the FLB pressure differential results in approximately a 10 percent increase in the leak rate relative to that which could be associated with normal plant operation. This small effect is reduced by the increased tube to tubesheet loading associated with the increased differential pressure. Thus, for a circumferential indication within the tubesheet region which is left in service in accordance with the pullout criterion (F*), the existing technical specification limit is consistent with accident analysis assumptions.

For axial indications in a full depth hardrolled tube below the bottom of the roll transition zone (which is assumed to remain in the tubesheet region), the tube end remains structurally intact and axial loads would be resisted by the remaining hardrolled region of the tube. For this case, the leak rate due to FLB differential pressure would be bounded by the leak rate for a free span leak source with the same crack length, which is the basis for the accident analysis assumptions.

For postulated accident conditions, the preload testing and analysis showed that a residual radial preload of about [ ]^{a,c,e} psi would exist between the tube and the tubesheet. In addition, the hydraulic proof test specimens did not leak, even at the minimum length of engagement, until applied pressures were significantly above those associated with accident conditions.

2.5.3 OPERATING PLANT LEAKAGE EXPERIENCE FOR TUBESHEET TUBE CRACKS

A significant number of tube tubesheet indications have been reported for some non-domestic steam generator units. The attitude toward operation with these indications present has been to tolerate them with no remedial action relative to plugging or sleeving. No significant number of shutdowns occurring due to leaks through these indications have been reported.

2.6 TUBE INTEGRITY UNDER POSTULATED LIMITING CONDITIONS

The final aspect of the evaluation is to demonstrate tube integrity under the postulated loss of coolant accident (LOCA) condition of secondary to primary differential pressure. A review of tube collapse strength characteristics indicates that the constraint provided to the tube by the tubesheet gives a significant margin between tube collapse strength and the limiting secondary to primary differential pressure condition, even in the presence of circumferential or axial indications.

The maximum secondary to primary differential pressure during a postulated LOCA is [ ]^{a,c,e} psi. This value is significantly below the residual radial preload between the tubes and the tubesheet. Therefore, no significant secondary to primary leakage would be expected to occur. In addition, loading on the tubes is axially toward the tubesheet and could not contribute to pullout.

2.7 CHEMISTRY CONSIDERATIONS

The concern that boric acid attack of the tubesheet due to the presence of a through wall flaw within the hardroll region of the tubesheet may result in loss of contact pressure assumed in the development of the F* Criterion is addressed below. In addition, the potential for the existence of a lubricated interface between the tube and tubesheet as a result of localized primary to secondary leakage and subsequent effects on the friction coefficient assumed in the development of the F* Criterion is also discussed.

2.7.1 TUBESHEET CORROSION TESTING

Corrosion testing performed by Westinghouse specifically addressed the question of corrosion rates of tubesheet material exposed to reactor coolant. The corrosion specimens were assembled by bolting a steel (A336) coupon to an Inconel Alloy 600 coupon. The coupon dimensions were 3 inches x 3/4 inch x 1/8 inch and were bolted on both ends. A torque wrench was used to tighten the bolts to a load of 3 foot-pounds.

The specimens were tested under three types of conditions:

1. Wet-layup conditions
2. Wet-layup and operating conditions
3. Operating conditions only

The wet-layup condition was used to simulate shutdown conditions at high boric acid concentrations. The specimens were exposed to a fully aerated 2000 ppm boron (as boric acid) solution at 140 degrees F. Exposure periods were 2, 4, 6, and 8 weeks. Test solutions were refreshed weekly.

While lithium hydroxide is normally added to the reactor coolant as a corrosion inhibitor, it was not added in these tests in order to provide a more severe test environment. Previous testing by Westinghouse has shown that the presence of lithium hydroxide reduces corrosion of Inconel Alloy 600 and steel in a borated solution at operating temperatures.

Another set of specimens were used to simulate startup conditions with some operational exposure. The specimens were exposed to a 2000 parts per million boron (as boric acid) solution for one week in the wet-layup condition (140 degrees F), and 4 weeks at operating conditions (600 degrees F, 2000 psi). During wet layup, the test solution was aerated but at operating conditions the solution was deaerated. The high temperature testing was performed in an Inconel autoclave. Removal of oxygen was attained by heating the solution in the autoclave to 250 degrees F and then degassing. This method of removing the oxygen results in oxygen concentrations of less than 100 parts per billion.

Additional specimens were exposed under operating conditions only for 4 weeks in the autoclave as described above.

High temperature exposure to reactor coolant chemistry resulted in steel corrosion rates of about 1 mil per year. This rate was higher than would be anticipated in a steam generator since no attempt was made to completely remove the oxygen from the autoclave during heatup. Even with this amount of corrosion, the rate was still a factor of nine less than the corrosion rate observed during the low temperature exposure. This differential corrosion rate observed between high and low temperature exposure was expected because of the decreasing acidity of the boric acid at high temperatures and the corrosive effect of the high oxygen at low temperatures.

These corrosion tests are considered to be very conservative since they were conducted at maximum boric acid concentrations, in the absence of lithium hydroxide, with no special precaution to deaerate the solutions, and they were of short duration. The latter point is very significant since parabolic corrosion rates are expected in these types of tests, which leads one to overestimate actual corrosion rates when working with data from tests of short duration.

Also note that the ratio of solution to surface area is high in these tests compared to the scenario of concern, i.e., corrosion caused by reactor coolant leakage through a tube wall into the region between the tube and the tubesheet.

2.7.2 TUBESHEET CORROSION DISCUSSION

At low temperatures, e.g., less than 140 degrees F, aerated boric acid solutions comparable in strength to primary coolant concentrations can produce corrosion of carbon steels. Deaerated solutions are much less aggressive and deaerated solutions at reactor coolant temperatures produce very low corrosion rates due to the fact that boric acid is a very much weaker acid at high temperature, e.g., 610 degrees F, than at 70 degrees F.

In the event that a crack occurred within the hardroll region of the tubesheet, as the amount of leakage would be expected to be insufficient to be noticed by leak detection techniques and is largely retained in the crevice, then a very small volume of primary fluid would be involved. Any oxygen present in this very small volume would quickly be consumed by surface reactions, i.e., any corrosion that would occur would tend to cause existing crevices to narrow due to oxide expansion and, without a mode for replenishment, would represent a very benign corrosion condition. In any event the high temperature corrosion rate of the carbon steel in this very local region would be extremely low (significantly less than 1 mil per year).

Contrast the proposed concern for corrosion relative to F* with the fact that Westinghouse has qualified boric acid for use on the secondary side of steam generators where it is in contact with the full surface of the tubesheet and other structural components made of steel. The latter usage involves concentrations of 5 - 10 ppm boron, but, crevice flushing procedures have been conducted using concentrations of 1000 to 2000 ppm boron on the secondary side (at approximately 275 degrees F where boric acid is more aggressive than at 610 degrees F).

Relative to the lubricating effects of boron, the presence of boric acid in water may change the wetting characteristics (surface tension) of the water but Westinghouse is not aware of any significant lubricating effect. In fact, any corrosion that would occur would result in oxides that would occupy more space than the parent metals, thus reducing crevice volume or possibly even merging the respective oxides.

3.0 SUMMARY

On the basis of this evaluation, it is determined that tubes with eddy current indications in the tubesheet region below the F* pullout criterion shown on Table 3. can be left in service. Tubes with circumferentially oriented eddy current indications of pluggable magnitude and located a distance less than F* below the bottom of the hardroll transition or the top of the tubesheet, whichever is greater relative to the top of the tubesheet, should be removed from service by plugging or repaired in accordance with the plant technical specification plugging limit. The conservativeness of the F* criterion was demonstrated by preload testing and analysis commensurate with the requirements of RG 1.121 for indications in the free span of the tubes, and by both pullout testing and hydraulic proof testing of thermally relaxed test specimens.

For tubes with axial indications, the criterion which should be used to determine whether tube plugging or repairing is necessary should be based on leakage since the axial strength of a tube is not reduced by axial cracks. Under these circumstances it has been demonstrated that significant leakage would not be expected to occur for through wall indications greater than [ ]^{a,c,e} inch below the bottom of the hardroll transition.

In addition, it has been determined, see Appendix II, that there is no need to stabilize tubes which are removed from service due to eddy current indications in the region between the top of the tubesheet and F*.

NOTE: The methodology for developing the F* criterion was first reported in a previous publication, reference 9, on the same subject. The difference being that the previously developed criterion, known as P*, was based on the available clearance for tube motion before it would be impeded by a neighboring tube or some other physical feature of the tube bundle. The values reported herein for F* are slightly larger than those reported for P*.

4.0 REFERENCES

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TABLE 1.

Model D Steam Generator Tube Roll Pre-Load Test - TEST DATA

Test No.	Location No.	Collar ID Pre-Roll			Collar OD Pre-Roll			Tube ID Before Roll			Tube OD Before Roll		
		0 Deg.	90 Deg.	Avg.	0 Deg.	90 Deg.	Avg.	0 Deg.	90 Deg.	Avg.	0 Deg.	90 Deg.	Avg. a,c,e
1	1												
	2												
	3												
	4												
	5												
	6												
	Average												
2	1												
	2												
	3												
	4												
	5												
	6												
	Average												
3	1												
	2												
	3												
	4												
	5												
	6												
	Average												
4	1												
	2												
	3												
	4												
	5												
	6												
	Average												
5	1												
	2												
	3												
	4												
	5												
	6												
	Average												
6	1												
	2												
	3												
	4												
	5												
	6												
	Average												
Col. Avgs:													

Notes: 1. All measured dimensions are in inches.
2. Column averages do not include Location Number 1.

TABLE 1. (CONT.)

Model D Steam Generator Tube Roll Pre-Load Test - TEST DATA

a,c,e

Test No.	Location No.	Pre-Roll Thickness	Collar OD Post-Roll 0 Deg.	Collar OD Post-Roll 90 Deg.	Collar OD Post-Roll Avg.	Collar Delta	Tube ID Post-Roll 0 Deg.	Tube ID Post-Roll 90 Deg.	Tube ID Post-Roll Avg.	Tube ID Growth	Tube ID Post-Roll Collar Removed 0 Deg.	Tube ID Post-Roll Collar Removed 90 Deg.	Tube ID Post-Roll Collar Removed Avg.
1	1												
	2												
	3												
	4												
	5												
	6												
	Average												
2	1												
	2												
	3												
	4												
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	Average												
6	1												
	2												
	3												
	4												
	5												
	6												
	Average												
Col. Avgs:													

Notes: 1. All measured dimensions are in inches.
2. Column averages do not include Location Number 1.

TABLE 1. (CONT.)

Model D Steam Generator Tube Roll Pre-Load Test - TEST DATA

a,c,e

Test No.	Location No.	Tube OD Post-Roll Collar Removed 0 Deg.	90 Deg.	Avg.	Post-Roll Thick	Thick-ness Red.	Orig. Gage Length	Gage Length Rolled	Gage Length Remov'd	Delta Length Percent	Radii Ratio (4)	Tube ID Spring-Back
1	1											
	2											
	3											
	4											
	5											
	6											
	Average											
2	1											
	2											
	3											
	4											
	5											
	6											
	Average											
3	1											
	2											
	3											
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	6											
	Average											
6	1											
	2											
	3											
	4											
	5											
	6											
	Average											
Col. Avgs:												

Notes: 1. All measured dimensions are in inches.
2. Column averages do not include Location Number 1.
3. The OD stress is calculated using the measured ID springback.
4. The radii ratio is a term that appears frequently in the analysis and is found as (OD^2+ID^2)/(OD^2-ID^2).

TABLE 2.

Model D Steam Generator Tube Roll Pre-Load Test - STRESS ANALYSIS RESULTS

Test No.	Location No.	Tube ID Spring-Back	Flex. dUi/dPo	Flex. dUo/dPo	OD Radial Stress	OD Hoop Stress	OD Axial Stress	Thermal Exp. Radial Stress	Flex. dUo/dPi	Oper. Pressure Radial Stress	Total Radial Stress	Total vonMises Stress
1	1											
	2											
	3											
	4											
	5											
	6											
	Average											
2	1											
	2											
	3											
	4											
	5											
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	6											
	Average											
6	1											
	2											
	3											
	4											
	5											
	6											
	Average											
Col. Avgs:												

a,c,e

Min =
Max =
St Dev =
St Dev = Based on (N-1)

Min =
Max =
St Dev =

Notes: 1. Column averages do not include Location Number 1.
2. The OD stress is calculated using the measured ID springback.
3. Test 2, Point 6 was omitted from the statistical parameter calculations.

TABLE 3.

Model D Steam Generator Tube Roll Pre-Load Test - PRELOAD ANALYSIS SUMMARY

Material Properties:

Elastic Modulus:	2.87E+07 psi
Poisson's Ratio:	0.30
I600 Expansion:	7.80E-06 in/in/F
T/S Expansion:	7.28E-06 in/in/F
Oper. Delta T:	550.00 F
Normal Delta P:	1400.00 psi
Faulted Delta P:	2650.00 psi

Additional Analysis Input:

Tubesheet Bow Stress Reduction

Normal:	psi
Faulted:	psi

a,c,e

Lower Tolerance Limit Factor:

95/95 LTL:	2.2324 (N = 29)

Tube/Tubesheet Dimensions (Tested):

Tube OD:	in.
Tube Thickness:	in.
Tubesheet ID:	in.
Thinning:	%

a,c,e

Coefficient of Friction:

End Effects:

Mean Rudius (Rolled):	in.
Thickness (Rolled):	in.
Lambda	
End Effect Length:	in.
Load Factor:	

EVALUATION OF REQUIRED ENGAGEMENT LENGTH

a,c,e

Elastic Analysis:	NORMAL	FAULTED
RT Preload (LTL)	psi	psi
Thermal Expansion Preload	psi	psi
Pressure Preload	psi	psi
Tubesheet Bow Loss	psi	psi
	---------	---------
NET Preload	psi	psi
NET Radial Force	lbs/in	lbs/in
NET Axial Resistance	lbs/in	lbs/in
	---------	---------
Applied Load:	lbs	lbs
Analysis Load:	lbs (SF = 3)	lbs (SF = 1.43)
End Effect Resistance (2):	lbs	lbs
NET Analysis Load:	lbs	lbs
Length Required:	in.	in.
TOTAL Length Required:	in.	in.
	=========	=========

NOTES:
1. 95/95 Lower Tolerance Limit Rolled Preload Used.
2. For NORMAL Operation a Safety Factor of 3 was Used.
3. For FAULTED Conditions a Safety Factor of 1.43 was Used Corresponding to ASME Code use of 0.7 on Ultimate Strength.
4. The Required Length Does NOT Include Eddy Current Inspection Uncertainty for the Location of the Bottom of the Hardroll, or the Top of the Tubesheet, Relative to the Degradation.

TABLE 4.

MODEL D STEAM GENERATOR ROLLED TUBE PULLOUT TESTS

Sample ID	Surface Rough. (RMS)	Engage Length (in)	Nom Reduct (%)	Actual Reduct (%)	Pullout Force (lbs)	Equiv Pres. (psi)	Ratio to Oper. Pres.	FLB Pres.	Comment

a,c,e

TABLE 5.

MODEL D STEAM GENERATOR ROLLED TUBE HYDRAULIC PROOF TESTS

Sample ID	Surface Rough. (RMS)	Engage Length (in)	Nom Reduct (%)	Actual Reduct (%)	Appl. Pres. (psi)	Equiv Force (lbs)	Ratio to Oper. Pres.	FLB Pres.	Comment

a,c,e

APPENDIX I - DISPOSITION OF TUBES WITH INDICATIONS ABOVE F*

Complementary to the criterion for leaving a tube in service with axial or circumferential indications below the top of the tubesheet is a criterion for determining the need to stabilize tubes which are removed from service due to circumferential indications below the top of the tubesheet. As was previously stated, ECT indications located above the F* criterion are to be dispositioned in accordance with the plant technical specification plugging limit which is based on USNRC RG 1.121, which does not distinguish between circumferential and axial cracks. Moreover, RG 1.121 is concerned with the depth of penetration of tube wall degradation, i.e., when the plugging limit is reached, the tube is either plugged or sleeved. RG 1.121 does not require stabilization of plugged tubes.

The kinetics of stress corrosion cracking of mill annealed Inconel 600 in primary water is highly temperature dependent. High temperatures accelerate rates of cracking. Laboratory measurements of Arrhenius relation type activation energies typically range from 30 to 75 kcal per mole. Field experience with row 1 U-bends in domestic steam generators and roll transitions in foreign units indicate an activation energy of 85 kcal per mole.

Conditions in tubes leading to lower tube metal temperatures greatly retard the kinetics of any subsequent cracking even if applied or residual stresses are maintained. Below an assumed temperature, T_{hot}, of 620 degrees F, cracking is retarded by a factor of 4 at 600 degrees F, a factor of 15.5 at 580 degrees F, and a factor of 64 at 560 degrees F. Moreover, the presence of hydrogen in primary water is another important consideration relative to the kinetics of cracking of Inconel 600. Laboratory measurements show that standard concentrations of hydrogen in primary water accelerates cracking by approximately a factor of 2 to 5 compared to control tests in the absence of hydrogen.

For use in a materials evaluation, in determining whether a tube plugged for an eddy current indication above the F* criterion should be stabilized due to the potential for continued growth of an ID stress corrosion crack, tube temperatures within and above the tubesheet region were assessed, refer to Appendix III. A plugged tube was postulated to exist in a variety of environments that would influence tube temperature, including the buildup of sludge around the tube, as the sludge may act as an insulator and alter the heat conduction patterns and surface metal temperature of the tube.

For conservatism, active tubes adjacent to the plugged tubes, and the tubesheet itself except at the secondary surface are assumed to be at primary fluid temperature. For this tubesheet temperature condition, several sludge deposition cases were hypothesized:

CASE 1 considers no sludge buildup adjacent to the tube and the tube does not have a through wall penetration prior to plugging. Certain conditions in tubes (such as wet walls prior to plugging) may lead to the presence of superheated steam existing within the tube. Limited data on Inconel 600 at high temperatures is consistent with general observations on aluminum and steel alloys in low temperature water vapor. At low superheat, i.e., high relative humidity, the cracking response in water vapor is essentially equivalent to that in the liquid phase at the same temperature, while at high superheat, i.e., low relative humidity, the cracking kinetics are much reduced. A plugged tube is essentially dry on its ID when plugged; therefore, although the ID temperature of the tube in the region within the tubesheet would most likely be equivalent to T_{hot}, the ratio of the vapor pressure of any water trapped in the tube during plugging to the pressure of saturated water vapor would be low, i.e., high superheat, thus greatly reducing the cracking kinetics. Also, as previously discussed, the lack of the presence of hydrogen in a plugged tube significantly retards further cracking. Therefore, combining the above two effects, the probability a plugged tube with degradation that has not progressed through wall would continue to degrade is small in this environment and would not require stabilization. It is noted that this case is really independent of whether or not sludge is postulated to be present, i.e., the temperature inside the tube in the tubesheet region will be near T_{hot} regardless of the presence of sludge.

CASE 2 considers no sludge buildup either adjacent to a plugged tube or in a plugged tube. A through wall indication is postulated and the tube is filled with water due to the ingress of secondary side water through the penetration. The water contained in the tube in the tubesheet area boils. This rapid heat transfer mechanism maintains the tube inner diameter metal surface at or slightly above T_{sat} for the portion of the tube in the tubesheet. The relatively low secondary side temperature will significantly inhibit the continuation and/or initiation of stress corrosion cracking. Therefore, considering both the effects of the reduced secondary side temperature and the lack of the presence of any hydrogen concentration on continued stress corrosion cracking, the probability of a plugged tube with a through wall penetration continuing to degrade is very small and would not require remedial action other than plugging or sleeving.

CASE 3 considers the effect of sludge buildup on the tubesheet adjacent to a plugged tube with a through wall penetration. Since the sludge acts as a poor conductor, the mechanisms for cooling the tube are not as efficient as for the previous two cases. If the secondary side water ingress remains primarily in liquid form with some localized boiling at the tube wall, and the sludge pile depth is less than about 4 inches, the temperature on the inner diameter of the tube will probably be slightly above T_{sat}. As discussed previously, certain conditions in plugged tubes may lead to the presence of superheated steam rather than liquid water the through wall degraded tube. It was also stated that at low superheat, the cracking response in water vapor is essentially equivalent to that in the liquid phase at the same temperature. At sludge depths greater than about 8 inches, the tube metal temperature in the tubesheet approaches plant hot leg temperature. The effect of low superheat and higher temperatures could result in additional crack growth. However, the above two scenarios are not expected to occur. In steam generators with a flow distribution baffle, sludge buildup to a height of 8 inches is precluded by geometry constraints. Moreover, as the postulated crack would most likely limit the ingress of the secondary side water in the through wall degraded tube, the most likely scenario would be that the tube is essentially dry on the inside and the ratio of the vapor pressure of the water to its saturation pressure is relatively low, thereby greatly reducing the crack kinetics. With the lack of the presence of any hydrogen concentrations, the potential for additional crack growth would be significantly reduced; therefore, tube stabilization is not required.

An extension to CASE 3 could be postulated such that the through wall penetration is of such size as to initially admit water into the plugged tube. The water subsequently boils and the internal pressure prevents any further water from entering the tube. In this case the steam would be at the secondary side pressure, i.e., high superheat conditions, and for reasons cited in consideration of case 1, further crack growth would not be expected.

APPENDIX II - TUBE WALL TEMPERATURES OF PLUGGED TUBES

To assess whether further degradation due to postulated PWSCC can occur in a plugged tube and to disposition tubes with indications above the F* criterion as to whether they should be stabilized when plugged, the metal temperature of the tube inside diameter at elevations above the F* criterion, but, below the top of the tubesheet, was evaluated. Active tubes adjacent to the plugged tube, and the tubesheet itself except at the secondary surface, are at the primary fluid temperature. For a tubesheet temperature condition equivalent to T_{hot}, five different sludge deposition cases were hypothesized.

1. An intact tube without sludge deposition on the tubesheet
2. A perforated tube without sludge on the tubesheet
3. An intact tube with sludge deposition on the tubesheet.
4. A perforated tube with sludge deposition on the tubesheet.
5. A perforated tube with/without sludge deposition on the tubesheet without secondary water ingress.

An intact tube is defined as a plugged tube with no through-wall penetration i.e., no secondary water comes in contact with the tube inner wall, while a perforated tube is defined as a tube with a through-wall penetration i.e., secondary water comes in contact with the tube inner wall.

1.1 Intact Tube Without Sludge Deposition

With the exception of a shallow layer at the tubesheet surface, the tubesheet metal temperature adjacent to active tubes just below the top surface of the tubesheet is expected to be at primary coolant inlet temperature (i.e. T_{hot}) for the hot leg side of the tube bundle. Therefore, the outer wall temperature can be as high as T_{hot} for a full depth hardroll expanded tube. For an intact tube, the inner wall of the tube is essentially dry. The inner wall maximum tube temperature along the length of the tubesheet would approach T_{hot}.

1.2 A Perforated Tube Without Sludge Deposition

Once a plugged tube is perforated, secondary water can ingress into the primary side of the inactive tube. The water contained in that portion of the tube within the tubesheet boils. This rapid heat transfer mechanism keeps the inner tube wall temperature at approximately T_{sat} + 5 ^{o}F (allowing for a localized wall superheat effect).

1.3 Intact Tube With Sludge Deposition

With sludge accumulation on the top of the tubesheet, the whole depth of the tubesheet is expected to be at T_{hot}. As is the case without sludge deposition, the inner wall of the inactive intact tube would be essentially dry with a maximum temperature of T_{hot} anticipated along the length of the tubesheet.

1.4 A Perforated Tube With Sludge Deposition

Similar to the case of a perforated tube with sludge deposition, secondary water can ingress into the primary side of the inactive tube. The heat transfer mechanism for cooling the tube inner wall metal temperature would be the same as with the case of no sludge deposition on the tubesheet. The inner wall temperature would be at approximately T_{sat} + 5 ^{o}F because of the boiling occuring inside the tube.

1.5 A Perforated Tube Without Communication

A situation could develop such that only a limited amount of secondary water would initially leak into a tube with a through-wall penetration. It can be postulated that the small amount of water ingressing into the tube inner diameter could evaporate and form superheated steam within the depth of the tubesheet or the tubesheet plus the height of the sludge. This case would be similar to an intact tube as the superheated steam would prevent the water from entering into the primary side of the tube. The inner wall of the tube would essentially be in a dry condition and the maximum inner wall metal temperature would be T_{hot}.

In summary, the inner wall temperature for the perforated tube within the depth of the tubesheet, both with or without sludge deposition, is essentially at T_{sat} + 5 ^{o}F when there is water communication due to a through wall penetration. The inner wall temperature for a perforated tube without water communication could be as high as T_{hot}. Finally, the inner wall temperature for an intact tube with or without sludge deposition could be as high as T_{hot}.