Westinghouse Non-Proprietary Class 3

WCAP-15431

Regulatory Guide 1.121 Analysis For Arkansas Nuclear One Unit 2 Replacement Steam Generators

Westinghouse Energy Systems

WCAP-15431 SG-00-06-005

Regulatory Guide 1.121 Analysis for Arkansas Nuclear One Unit 2 Replacement Steam Generators

June, 2000

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ABSTRACT

This report describes the analysis to determine tube repair limits for the Arkansas Nuclear One Unit 2 (ANO-2) Delta 109 replacement steam generator tubing. Based on the analysis results, a minimum tube thickness requirement in percent of the nominal wall is established in accordance with the guidelines of the USNRC Regulatory Guide 1.121. In order to establish a final repair limit, an allowance is included to account for uncertainties in eddy current measurements and continued tube wall degradation between consecutive inspection periods.

LIST OF ABBREVIATIONS

Abbreviation	Description
ASME	American Society of Mechanical Engineers
AVB	Anti-Vibration Bar
CSD	Cold Shutdown
DBE	Design Basis Earthquake
EC	Eddy Current
FIV	Flow-Induced Vibration
FLB	Feedline Break
FS	Factor of Safety
LOCA	Loss of Coolant Accident
NDE	Non-Destructive Examination
NSSS	Nuclear Steam Supply System
PWR	Pressurized Water Reactor
RG	Regulatory Guide
SLB	Steamline Break
T/H	Thermal-Hydraulic
TSP	Tube Support Plate
US NRC	United States Nuclear Regulatory Commission
ΔP	Thinned Tube Burst Strength
ΔP_i	Primary-to-Secondary Pressure Gradient
ΔP_o	Secondary-to-Primary Pressure Gradient
ΔP^o	Unthinned Tube Burst Strength
a	Crack Length
A_{min}	Degraded Tube Area
A_{nom}	Nominal Tube Area
d	Depth of Thinning
deg F	degrees Fahrenheit
DP	Pressure Drop
h	Depth of Thinning
I_{min}	Degraded Area Moment of Inertia
in	Inch
I_{nom}	Nominal Area Moment of Inertia
K	Shape Factor
ksi	kips per square inch
L	Length of Thinned Region
lbs	Pounds

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LIST OF ABBREVIATIONS (Continued)

Abbreviation	Description
λ	Normalized Crack Length
OD_{max}	Maximum Tube Outside Diameter
OD_{min}	Minimum Tube Outside Diameter
°F	degrees Fahrenheit
P_b	Primary Bending Stress
P_c	Collapse Pressure
P_i	Primary Side Pressure
P_m	Primary Membrane Stress
P_N	Normalized Burst Pressure
P_o	Secondary Pressure
psi	pounds per square inch
psia	pounds per square inch atmospheric (pressure)
Q	Secondary Stress
R_i	Tube Inside Radius
r_m	Tube Mean Radius
R_o	Tube Outside Radius
SIG	Principal Stress
S_m	Allowable Stress Intensity
S_u	Ultimate Strength
S_y	Yield Strength
t	Tube Wall Thickness
t_{min}	Tube Minimum Wall Thickness

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SECTION 1

INTRODUCTION

1.1 Regulatory Requirements for Tube Repair

The heat transfer area of steam generators in a PWR nuclear steam supply system (NSSS) comprises over 50 percent of the total primary system pressure boundary. The steam generator tubing, therefore, represents a primary barrier against the release of radioactivity to the environment. For this reason, conservative design criteria have been established for the maintenance of tube structural integrity under the postulated design-basis accident condition loadings in accordance with Section III of the ASME Code, Reference 1.

Over a period of time under the influence of the operating loads and environment in the steam generator, some tubes may become degraded in local areas. To determine the condition of the tubing, in-service inspection using eddy-current techniques is performed in accordance with the guidelines of US NRC Regulatory Guide 1.83, Reference 2. Partially degraded tubes are satisfactory for continued service provided that defined structural and leakage limits are satisfied, and that the prescribed structural limit is adjusted to take into account possible uncertainties in the eddy current inspection, and an operational allowance for continued tube degradation until the next scheduled inspection.

The US NRC Regulatory Guide 1.121, Reference 3, describes an acceptable method for establishing the limiting safe conditions of degradation in the tubes beyond which tubes found defective by the established in-service inspection shall be removed from service. The level of acceptable degradation is referred to as the "repair limit".

Briefly, the regulatory guideline consists of verifying that

1. In the case of (uniform) tube thinning or wall loss, the remaining tube wall can still meet applicable stress limits during normal and accident loading conditions,
2. For tube cracking, that margins against tube burst are satisfied, and
3. In the case of tube cracking, that Tech Spec leakage limits are satisfied.

The purpose of this evaluation is to define the "structural limit" for an assumed uniform thinning mode of degradation in both the axial and circumferential directions. The assumption of uniform thinning is generally regarded to result in a conservative structural limit for all flaw types occurring in the field. The allowable tube repair limit, in accordance with Regulatory Guide 1.121, is obtained by incorporating into the resulting structural limit, a growth allowance for continued operation until the next scheduled inspection and also an allowance for eddy current measurement uncertainty.

1.2 Scope of the Repair Limit Analysis

This report describes the results of an analysis performed for the Arkansas Nuclear One Unit 2 replacement steam generator tubing in order to establish the tube repair limits. Arkansas Nuclear One Unit 2 is a two-loop NSSS, with a Delta 109 replacement steam generator in each loop. A schematic of a Delta 109 steam generator is shown in Figure 1-1. All tubing in the Arkansas Nuclear One Unit 2 replacement steam generators is thermally treated Alloy 690 (SB-163). The nominal tube geometry is 0.688 inch OD by 0.040 inch t.

This evaluation is applicable to the integrity of individual tubes with both general and local degradation. General degradation is treated by a nominal reduction in thickness over its entire length. Local degradation is conservatively assumed to be uniform thinning in both the axial and circumferential directions, or as a single axial through-wall or partial-depth crack. Criteria are categorized into three tube regions, anti-vibration bar (AVB) intersections, support plate intersections, and straight leg regions of the tube.

The assumption of uniform thinning results in development of a repair limit that is conservative for all flaw types occurring in the field, such as pits, short cracks, and outside diameter stress corrosion cracking that occurs at tube support plates. The repair limit criteria developed herein are not applicable to circumferential cracks. Circumferential cracks, should they occur, must be considered through a degradation specific program.

The evaluation basically consists of tube load determination, tube stress analysis, minimum tube wall thickness determination, and confirmation of leak-before-break. The leak-before-break confirmation makes use of test data on leakage rates and burst strength as a function of through-wall crack length. The data is available from several programs for establishing characteristics of degraded Alloy 600 tubing.[1]

Cracking of steam generator tubing is usually the result of corrosion mechanisms and the cracks propagate as a result of continued corrosion rather than by the loads induced during operation. Burst testing of tubes with through-wall cracks show that they do not fail in a brittle manner but by plastic instability, or fishmouthing, of the cracked region. It is for these reasons that burst testing has become the standard for demonstrating tube strength. Leakage through these tight cracks is also determined by testing to provide as realistic a leak rate as possible. The leak rate tests are performed in the laboratory at steam generator pressure and temperature conditions.

In connection with the tube bundle integrity evaluation, it should be noted that both the safety and functional requirements are to be satisfied. The safety requirement, which is the basis of the Regulatory Guide 1.121 criteria, governs the limiting safe condition of the localized tube degradation, as established by in-service inspection, beyond which tubes

[1] Reference 4 also provides leak and burst test data that shows Alloy 690 to behave almost identically to Alloy 600.

should be repaired or removed from service. In contrast, the functional requirement applies to the overall degradation of the tube bundle. Although both the safety and functional requirements are evaluated as part of this analysis, the subject matter of this analysis deals mainly with the safety requirements associated with the repair limit criteria in Regulatory Guide 1.121.

Regarding the remainder of this document, specific criteria and the corresponding allowable limits and/or margins associated with the safety and functional requirements are discussed in Section 2. Details of tube loadings during the various plant conditions and a summary of the tube evaluation methodology are discussed in Section 3. Section 4 contains a summary of the analysis results for overall bundle integrity, and Section 5 summarizes the calculations to determine the applicable structural limit. Finally, Section 6 presents a summary of the structural limits and associated repair limits, with report references listed in Section 7.

a,c

Figure 1-1
Schematic of a Delta 109 Steam Generator

SECTION 2

INTEGRITY REQUIREMENTS AND CRITERIA

The steam generator tubing represents an integral part of the primary barrier against the release of radioactivity into the atmosphere. In the event of a primary loss-of-coolant accident (LOCA), the tubing also provides the necessary heat sink, initially, for the core cooldown, and later for maintaining the plant in the safe shutdown condition. Thus, it is important to establish the structural integrity of the steam generator tubing by requiring that, based on analyses, testing, and in-service inspection, the tube bundle sustain, with recommended margins, the loads during normal operation and the various postulated accident conditions, without a loss of function of safety.

2.1 Functional and Safety Requirements

Tube walls may be affected by a number of different factors such as environment-induced corrosion (including intergranular attack and stress-corrosion cracking), erosion due to the fluid friction, and wear from mechanical and flow-induced vibrations. The wall loss due to general erosion or corrosion has been conservatively established and is assumed to be more or less uniform for the entire tube bundle during the plant operating period. However, a potential for additional wall degradation may exist locally in some tubes in the region of tube/tube support plate and tube/AVB intersections because of a higher potential for chemical concentrations and/or relative motion in these regions.

Based on steam generator operational history, the majority of the tubes are expected to be subjected to only a small, but probably a more or less uniform, tube wall loss over the design life of the unit. On the other hand, some tubes of the bundle may degrade locally to the extent that either the removal of these tubes from service or local repair to restore integrity is necessary for continued safe operation of the unit. Because of these two distinct modes of tube degradation, it is possible to separate the functional and safety requirements into those affecting the integrity of (1) the overall tube bundle, and (2) a locally-thinned or degraded tube. In evaluating the overall bundle for general erosion and corrosion, an end-of-life general erosion on the inside of the tube is assumed to be [ ]a,c inch, and a general corrosion on the outside of the tube is assumed to be [ ]a,c inch.

2.2 Overall Tube Bundle Integrity Requirements

These requirements are based on the assumption that removal of tubes from service does not impair the structural and functional capability of the overall tube bundle. In the event of extensive tube plugging, plant derating and/or re-analyses associated with functional requirement verification may be necessary. However, re-analysis for the verification of the structural integrity of the tube bundle as a whole will not be required, since the deactivated tubes would physically remain in the tube bundle, thus maintaining the structural characteristics of the tube bundle practically intact. Removal of an isolated tube for inspection purposes would have an insignificant affect on the overall bundle response.

2.3 Locally-Degraded Tube Integrity Requirements

As previously indicated, the potential for localized tube wall degradation may exist at certain locations in the tube bundle. Even though such localized degradation is known to be confined over a small portion of the tubing (and hence of no adverse consequence to the functional capability of the bundle), it is to be assessed from the viewpoint of a potential tube burst, if the associated depth of penetration is relatively large. Therefore, to show that there are no safety consequences as a result of a random tube burst, a conservative bound on acceptable degradation for continued operation must be established along with the in-service inspection and leakage monitoring requirements for the detection of degraded tubes. Guidelines in US NRC Regulatory Guide 1.83 for EC inspection and US NRC Regulatory Guide 1.121 for tube repair limit calculations provide the bases for determining the limiting safe condition of a locally-degraded tube. For tube degradation in excess of the established repair limit, it is required that the tube be repaired or removed from service in order to provide continued safe operation.

The intent of US NRC Regulatory Guide 1.121, as applicable to this analysis, is as follows:

- In the case of tube thinning due to the mechanical and chemical wastage, and generalized intergranular attack, stresses in the remaining tube wall must be capable of meeting the applicable requirements with adequate allowance for the EC measurement uncertainties and assumed continued degradation until the next scheduled outage. The strength requirements are specified in terms of allowable primary stress limits and margins against burst during normal operation and collapse following a LOCA.
- For tube cracking, the tube must meet margins against burst under normal operation and postulated accident conditions. In addition, the accumulated leak rate through all degraded tubes must meet tech spec limits. If the accumulated leak rate exceeds the specification, the plant must be shut down and corrective actions taken to restore integrity of the unit.

2.4 Tube Stress Classification

For plants in seismic regions, the most limiting loads for establishing the tube integrity are imposed during the Level D Service Conditions; that is, LOCA + DBE, FLB (Feedline Break) + DBE and SLB (Steamline Break) + DBE. In order to evaluate the stresses, the stresses must be classified consistent with the definitions in the ASME Code. There are two general considerations that must be accounted for in determining the classification of stresses, namely the location in the structure and the nature of the loading.

The tube stress classifications for various locations in the tube bundle under the different types of loadings are summarized in Table 2-1. The notation "P_m" refers to general primary membrane stress, "P_b" refers to primary bending stress and "Q" refers to secondary stress.

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]a,c

2.5 Criteria and Stress Limits

The allowable stress limits are established using the ASME Code minimum strength properties. A summary of the corresponding tube strength properties is provided in Table 2-2.

Levels A and B Plant Conditions

The limits on primary stress, P_m, for a primary-to-secondary pressure differential ΔP_i, are as follows: Note that the analysis is performed based on the properties at 600°F.

$$\text{Normal Operation: } P_m < S_u/3$$

$$\text{Transient Conditions: } P_m < S_y$$

Level D (Postulated Accident) Conditions

Loadings associated with a primary (LOCA) or a secondary side (FLB/SLB) blowdown, concurrent with the DBE, are evaluated against the stress limits specified for Level D Service Conditions in Appendix F of the Code. Since the tube has a circular cross-section, the shape factor K is introduced in determining the allowable membrane plus bending stress.

$$P_m < \text{smaller of } 2.4\ S_m,\ 0.7\ S_u$$

$$P_m + P_b < K\ S_m$$

The shape factor K, is the ratio of the moment to cause yielding of the full cross-section, assuming elastic-plastic material behavior, to the moment to cause yielding of the tube outer fiber. For a circular cross-section, the shape factor, K, has the following relationship.

$$K = \frac{16R_o}{3\pi}\left(\frac{R_o^{\,3} - R_i^{\,3}}{R_o^{\,4} - R_i^{\,4}}\right)$$

where,

R_o = Tube Outside Radius
R_i = Tube Inside Radius

For two-sided AVB wear, the shape factor, K, has the following relationship for in-plane bending. Recall that out-of-plane bending in the U-bend is secondary for localized tube wear.

$$K = \frac{16R_o}{3}\left(\frac{R_o^{\,3} - R_i^{\,3}}{\pi\left(R_o^{\,4} - R_i^{\,4}\right) - 8I_s}\right) - \frac{8R_o\left(R_o d^2 - \frac{1}{3}d^3\right)}{\pi\left(R_o^{\,4} - R_i^{\,4}\right) - 8I_s}$$

where,

$$I_s = \frac{AR_o^{\,2}}{4}\left\{1 - \frac{2}{3}\left[\frac{\sin^3\alpha\cos\alpha}{\alpha - \sin\alpha\cos\alpha}\right]\right\}$$

$$A = \frac{R_o^{\,2}}{2}\left(2\alpha - \sin 2\alpha\right)$$

$$\alpha = \cos^{-1}\left(\frac{R_o - d}{R_o}\right)$$

and,

R_o = Tube outside radius
R_i = Tube inside radius
d = Depth of thinning
α = Arc length spanned by wear scar

Tables 2-3 and 2-4 provide a summary of the shape factor, K, versus depth of thinning for uniform and two-sided wear, respectively.

A summary of the allowable stresses for the various operating conditions is provided in Table 2-5. Once preliminary values are established for the minimum cross-sections, then allowable stresses are calculated for the locally degraded cross-sections, as appropriate, and the minimum cross-sections are evaluated against those allowables.

As far as the consideration of the secondary and peak stresses in the evaluation of a locally thinned tube is concerned, it is noted that the effects of these stresses will be manifested in ratcheting, fatigue and/or corrosion-fatigue type of mechanisms associated with tube cracking if that should occur. In that case, the Tech Spec limits on allowable leakage would implicitly guard against the effects of the secondary and peak stresses.

Table 2-1
Tube Stress Classification

a,c

Table 2-2
Tube Strength Properties
Thermally Treated Alloy 690
0.688" OD x 0.040" t

Temperature (°F)	S_y (1) (ksi)	S_u (2) (ksi)	S_m (1) (ksi)
100	40.0	80.0	26.6
200	38.2	80.0	26.6
300	37.3	80.0	26.6
400	36.3	80.0	26.6
500	35.7	80.0	26.6
600	35.3	80.0	26.6
700	35.0	80.0	26.6

(1) Values based on Code Case N-20-3 (Reference 15)

(2) Values for S_u at elevated temperatures inferred from criteria in the ASME Code Appendix III-2110 (b) for S_m, and minimum tensile strength specified in Code Case N-20-3

Table 2-3
Calculation of Tube Shape Factor (K) as a Function of Thinning

Case of Uniform Thinning

a,c

Table 2-4
Calculation of Tube Shape Factor (K) as a Function of Thinning

Two-Sided AVB Wear: In-Plane

a,c

Table 2-5
Summary of Allowable Stresses

a, c

SECTION 3

PRIMARY LOADS FOR TUBE ANALYSIS

In establishing the safe limiting condition of a tube in terms of its remaining wall thickness, the effects of loadings during both normal operation and postulated accident conditions must be evaluated. The applicable stress criteria are in terms of allowables for the primary membrane and membrane-plus-bending stress intensities. Hence, only the primary loads (loads necessary for equilibrium) need to be considered.

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]a,c

3.1 Normal Operation and Normal / Upset Transient Loads

The applicable normal operation and transient conditions for this analysis are defined in Westinghouse steam generator equipment specification Reference 6. The limiting stresses for normal operation and operating transient conditions are the primary membrane stresses due to the primary-to-secondary pressure differential ΔP_i across the tube wall. A summary of the normal operation (100% Power) parameters, as defined in Reference 6, is provided in Table 3-1. The normal / upset transient parameters are also defined in References 6. A summary of the transient parameters is provided in Table 3-2.

3.2 Accident Condition Loads

For the accident condition evaluation, the postulated loading events are: Loss-of-Coolant Accident (LOCA), Main Steam Line Break (SLB), Main Feed Line Break (FLB) and Design Basis Earthquake (DBE). The tube integrity evaluation is performed for the blowdown loads in conjunction with the DBE loads, i.e.: LOCA+DBE and FLB/SLB+DBE. The initial conditions for these events correspond to 100% full power condition thus maximizing the resulting tube loadings.

3.2.1 LOCA Loads

LOCA loads are developed as a result of transient flow and pressure fluctuations following a postulated main coolant pipe break. Based on the prior qualification of Arkansas Nuclear One Unit 2 for leak before break requirements for the primary piping, the limiting

LOCA event is the Shutdown Cooling line break. As a result of a LOCA, the steam generator tubing is subjected to three distinct types of loading mechanisms:

1) Primary fluid rarefaction wave loads,
2) Steam generator shaking loads due to the coolant loop motion and,
3) External hydrostatic pressure loads as the primary side blows down to the atmospheric pressure.

The first two loading mechanisms occur simultaneously during the course of LOCA and result predominantly in bending stresses in the tube U-bends at the top TSP. In contrast, the maximum secondary-to-primary pressure differential occurs during the quasi steady-state portion of the transient and, therefore, its effects on tube integrity can be evaluated independently of the first two loads. The main concern with this loading is tube collapse potential and the consequent increase in the primary flow resistance to the extent that core cooldown rate is affected.

LOCA Rarefaction

The LOCA rarefaction wave initiates at the postulated break location and travels around the tube U-bends. A differential pressure is created across the two legs of the tubes, which causes an in-plane horizontal motion of the U-bend. The integrated response of the tube bundle to the individual tube loads results in significant lateral loads on the tubes.

The pressure-time history input to the structural analysis is obtained from a transient thermal-hydraulic (T/H) analysis using the CEFLASH-4A computer code, Reference 7. A break opening time of 1.0 msec of full flow area, simulating an instantaneous double-ended rupture is assumed to obtain conservative hydraulic loads. Pressure time histories are calculated for three tube radii, identified as the minimum, average and maximum radius tubes. A plot showing the tube representation in the T/H model is provided in Figure 3-1.

The limiting small pipe break is the Shutdown Cooling (SDC) line. Plots of the pressure drops for the hot-to-cold leg tangent points for the minimum, average, and maximum radius tubes are shown in Figures 3-2, 3-3, and 3-4, respectively. A summary of the maximum pressure drops for the three tube sizes is provided in Table 3-3.

For the rarefaction wave induced loadings, the predominant motion of the U-bends is in the plane of the U-bend. Thus, the anti-vibration bars do not couple the individual tube motions. Also, only the U-bend region is subjected to high bending stresses. Therefore, the structural analysis is performed using single tube models limited to the U-bend and the straight leg region over the top two TSP's. A schematic of the tube structural model is shown in Figure 3-5.

The analysis considers nine different tube radii, as summarized in Table 3-4. In addition to the three tube sizes considered in the LOCA thermal/hydraulic analysis, six intermediate tube sizes are also considered. For the intermediate tube sizes, the pressure time history from the next larger tube size is used. For instance, for the 17.5 inch radius tube, the pressure time history from the average radius tube is used. Since the magnitude of the hot to cold leg pressure drop is approximately proportional to the tube radius, this is judged to result in conservative loads for the intermediate tubes.

In performing the dynamic analysis, the mass inertia of the tube is input as effective material density and includes the weight of the tube, weight of the primary fluid inside the tube and the hydrodynamic mass effects of the secondary fluid. Damping coefficients are defined to realize a maximum damping of 3% at the lowest and highest significant frequencies of the structure.

When evaluating large break LOCA events, [

]a,c

LOCA Shaking

Concurrent with the rarefaction wave loading during a LOCA, the tube bundle is subjected to additional bending loads due to the shaking of the steam generator caused by the break hydraulics and the resulting time history displacements imposed on the steam generator. Hydraulic forcing functions are applied to a system structural model, which includes the steam generator, the reactor coolant pump and the piping. This analysis yields the time history displacements of the steam generator at its upper lateral and lower support nodes. Using the time history displacements at the supports as inputs, a nonlinear time-history analysis for the steam generator is performed. A non-linear analysis is used to account for the effects of radial gaps between the secondary shell, the wrapper, and the TSP. The analysis is performed using the the WECAN computer program, Reference 8, and the same model as for the seismic analysis, described below.

3.2.2 FLB/SLB Loads

During the postulated FLB/SLB accidents, the predominant primary tube stresses result from the ΔPi loading. The peak differential pressures for these events are obtained from the results of transient blowdown analyses. The secondary side blowdown transients are based on an instantaneous full double-ended rupture of the main feedline / steamline. In both cases, the secondary side of the faulted steam generator blows down to the ambient pressure. A peak transient pressure differential of [ ]a,c psi is used as an umbrella load for the stress evaluation of these two events.

In addition to the primary to secondary pressure gradient, in a manner similar to the LOCA shaking loadings, the tube bundle is subjected to additional bending loads due to the shaking of the steam generator caused by the break hydraulics and the resulting time history displacements imposed on the steam generator. Hydraulic forcing functions are applied to a system structural model that includes the steam generator, the reactor coolant pump and the piping. This analysis yields time history displacements of the steam generator at its upper lateral and lower support nodes. The resulting time-history displacements formulate the forcing functions for obtaining the tube stresses due to FLB and SLB shaking of the steam generator. Again, the tube stresses are calculated using the WECAN computer code and the finite element model developed for the seismic analysis.

During an SLB event, the flow conditions may produce a pressure gradient across the outside of the tube U-bend (intrados to extrados). The resulting tube stresses for this condition were determined by constructing a finite element model of the largest U-bend radius tube using the WECAN computer code and applying a conservative pressure gradient (0.15 psid) across the tube. The resulting tube in-plane bending stresses for this loading condition were found to be small, less than [ ]a,c ksi. This bending stress is conservatively applied to each tube, independent of the tube radius, or the location along the tube.

Finally, bending of the tube may occur as a result of flow-induced vibration. Tube bending stresses have been subject to conservatively defined full-power uprated operating conditions. Limiting out-of-plane tube bending stresses for steam line break were scaled by the ratio of pressure drops (faulted/full-power) in appropriate regions of the steam generator to estimate limiting faulted condition bending stresses. The resulting out-of-plane bending stresses for loading during SLB were found to be a maximum of [ ]a,c ksi for a nominal tube. This bending stress is again conservatively applied to each tube, independent of the tube radius, or the location along the tube.

3.2.3 DBE Loads

Seismic loads due to a Design Basis Earthquake (DBE) are developed as a result of the motion of the ground during an earthquake. A nonlinear time-history analysis is used to account for the effects of radial gaps between the secondary shell, the wrapper, and the TSP. The seismic excitation defined for the steam generators is in the form of time history displacements at the steam generator supports. The displacements were developed from a seismic analysis of the reactor coolant system including the steam generators.

The seismic analysis is performed using the WECAN computer program, Reference 8. The mathematical model consists of three-dimensional lumped mass, beam, and pipe elements to provide a plant specific representation of the steam generator. In the nonlinear analysis, the TSP/shell, TSP/wrapper, and wrapper/shell interactions are represented by concentric spring-gap dynamic elements, using impact damping to account for energy dissipation at these locations. The mathematical model that is used is shown in Figure 3-6.

Two equivalent beams model the straight leg region on both the hot-leg side and cold-leg side of the tube bundle. The U-bend region, however, is modeled as five equivalent tubes of different bend radii, each equivalent tube representing a group of steam generator tubes. In addition, a single tube representing the outermost tube row was also modeled. The values of the equivalent U-bend radii are determined based on how various groups of tubes contact the anti-vibration bars during the out-of-plane motion of the tube bundle. Continuity between the straight leg and U-bend tubes, as well as between the U-bend tubes themselves, is accomplished through appropriate nodal couplings.

Table 3-1
Summary of Normal Operation Parameters
100% Power

a, c

Table 3-2
Summary of Transient Parameters
Normal (Level A) / Upset (Level B) Conditions

a, c

Table 3-3
Summary of Maximum Hot Leg-to-Cold Leg Pressure Drops
Shutdown Cooling Line Break

a, c

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Table 3-4
Summary of Model Parameters for LOCA Model

a, c

a, c

Figure 3-1
Thermal / Hydraulic Model for LOCA Analysis

a, c

Figure 3-2
Hot-to-Cold Pressure Differential
Shutdown Cooling Line Break
Minimum Radius Tube

a, c

Figure 3-3
Hot-to-Cold Pressure Differential
Shutdown Cooling Line Break
Average Radius Tube

a, c

Figure 3-4
Hot-to-Cold Pressure Differential
Shutdown Cooling Line Break
Maximum Radius Tube

a, c

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Dimension "D" from center of "H" TSP to the U-bend Tangent Point for the different radius tube models are:

Short:	D = 3.1325 inches
INTAS1:	D = 3.8075 inches
INTAS2:	D = 4.4825 inches
INTAS3:	D = 5.1585 inches
Average:	D = 5.8535 inches
INTLA1:	D = 7.3335 inches
INTLA2:	D = 9.3135 inches
INTLA3:	D = 11.7735 inches
Long:	D = 14.9535 inches

Figure 3-5
Tube Finite Element Model
LOCA Rarefaction Wave Analysis

a, c

Figure 3-6
Seismic Model Representation of Steam Generator

SECTION 4

TUBE EVALUATION

OVERALL BUNDLE INTEGRITY

4.1 Functional Integrity Evaluation

Calculations are performed to verify that the functional requirements associated with the overall tube bundle integrity during and following the Level D service condition loadings are satisfied; i.e., the primary stresses are within the limits of Appendix F of Section III of the Code. For primary membrane plus bending stresses, the tubes are evaluated for FLB+DBE and LOCA+DBE.

4.1.1 LOCA + DBE

For primary membrane plus bending stresses, the tubes are evaluated for FLB+DBE and LOCA+DBE. A summary of the maximum seismic membrane stresses is provided in Table 4-1. Stresses are summarized for the U-bend, top tube support plate (TSP), and for the straight leg region of the tube. Note in Table 4-1 that dead weight and seismic stresses have been considered separately. The seismic stresses have been calculated through a square root of the sum of the squares summation of the maximum stress for each of the three orthogonal excitation directions. Thus, it is not possible to assign a sign (tension or compression) to the axial seismic stress. Therefore, the dead weight stresses are combined in a conservative manner, assuming that they are tensile and of the same sign as the seismic stress. A summary of the maximum seismic bending stresses is provided in Table 4-2, differentiating between in-plane and out-of-plane response.

A summary of the maximum rarefaction wave induced bending stresses for the LOCA Shutdown Cooling Line break for the top tube support plate and U-bend locations is provided in Table 4-3. In order to combine the LOCA and seismic stresses, the LOCA stresses are linearly interpolated to give approximate stresses at radii corresponding to the radii in the seismic analysis.[2] A summary of the interpolated stresses for LOCA rarefaction is provided in Table 4-4. Recall that in-plane bending stresses at the top TSP

[2] The appropriateness of the linear approximation lies in the available margins relative to the allowables for the combined stresses. Excluding the minimum radius tube, the stresses for the remainder of the tubes analyzed range from 4800 psi to 10,200 psi, a range of 5200 psi. It is shown below that the combined LOCA and seismic stress is less than 50 ksi, with an allowable that exceeds 75 ksi. Additional analysis to develop a more exact approximation technique is not warranted.

are secondary and do not need to be evaluated for primary stress limits. Summaries of the maximum membrane and bending tube stresses for LOCA shaking are provided in Tables 4-5 and 4-6, respectively. Table 4-7 provides a summary of the combined LOCA rarefaction and LOCA shaking stresses. The stresses are combined directly, assuming that the limiting stresses occur at the same time during the transient.

The combined LOCA+DBE membrane + bending stresses at the top tube support plate are shown in Table 4-8. (Note that tube stresses due to the through-wall pressure gradient are not included. These stresses are calculated and combined with LOCA / DBE stresses in subsequent tables.) For both the seismic and LOCA conditions, the membrane and bending stresses are combined on a linear basis assuming the maximums occur at the same time during the transient. The combined LOCA+DBE stress is calculated using the square root of the sum of the squares.

For the U-bend region of the tube, where both the in-plane and out-of-plane stress are classified as primary for the nominal tube, it is necessary to determine the tube stress as a function of the azimuthal position around the tube circumference. At any given angle around the tube circumference, the combined LOCA+DBE membrane + bending stress is calculated as follows:

$$\sigma_a = \{[\sigma_b(\text{DBE-In-Plane}) \cos\theta + \sigma_b(\text{DBE-Out-of-Plane}) \sin\theta + \sigma_m(\text{DBE})]^2 + [\sigma_b(\text{LOCA-In-Plane}) \cos\theta + \sigma_b(\text{LOCA-Out-of-Plane}) \sin\theta + \sigma_m(\text{LOCA})]^2\}^{1/2}$$

The combined LOCA+DBE membrane + bending stresses at the top tube support plate are shown in Table 4-9.

Tube stresses resulting from the through-wall pressure gradient are calculated using the following closed form solutions.

$$\sigma_{hoop} = \frac{P_i R_i - P_o R_o}{t}$$

$$\sigma_{axial} = \frac{P_i R_i^2 - P_o R_o^2}{R_o^2 - R_i^2}$$

For the LOCA+DBE case, the through-wall pressure stresses are conservatively calculated for full power operating conditions, which correspond to the transient initial conditions. As a result of the break, the primary side de-pressurizes, resulting in a decrease in the primary-to-secondary pressure drop from the transient initial conditions. For the Feedline Break transient, the through-wall pressure stresses are calculated for the maximum primary to secondary ΔP loading. A summary of the resulting tubes stresses for through-wall pressure gradients corresponding for Full Power and Feedline Break conditions is provided in Table 4-10.

The combined LOCA+DBE stresses are shown in Table 4-11 for the U-bend region and in Table 4-12 at the top of the uppermost tube support plate. Summaries of the tube stress intensities for the LOCA+DBE conditions are provided in Tables 4-13 and 4-14 for U-bend and top TSP locations, respectively. The maximum stress intensity has a value of [ ]^{a,c} ksi.

4.1.2 FLB + DBE

As discussed earlier, there are several loading contributions to the tubes during a FLB event. The predominant loading is due to the through-wall pressure gradient that occurs. Additional tube stresses result from overall steam generator shaking, flow induced vibration (FIV) loads, and a pressure drop across the outside of the tube (intrados-to-extrados). As discussed above in Section 3.2.2, the FIV induced out-of-plane bending stress is conservatively calculated to be [ ]^{a,c} ksi. The in-plane bending stress due to the pressure drop across the OD of the tube has been conservatively calculated to be [ ]^{a,c} ksi, as also discussed in Section 3.2.2. The shaking induced stresses are conservatively taken to be equal to the stresses calculated for LOCA shaking. A summary of the combined FLB axial stress, incorporating each of the above stress contributions is provided in Table 4-15.

The seismic stresses are combined with the FLB stresses using the same methodology as for the LOCA+DBE load combination. A summary of the combined FLB+DBE stresses at the top TSP is provided in Table 4-16, and the combined stresses in the U-bend are documented in Tables 4-17 for each of the tube radii.

The overall combined FLB+DBE stresses are summarized in Tables 4-18 and 4-19 for the U-Bend and TSP, respectively. Summaries of the corresponding tube stress intensities for the FLB+DBE conditions are provided in Tables 4-20 and 4-21. The maximum stress intensity has a value of [ ]^{a,c} ksi.

4.1.3 Combined Stress – End-of-Life Condition

To account for the loss of material due to general erosion and corrosion, the maximum stress intensity is conservatively scaled upward by the ratio of the area moments of inertia for the nominal and reduced cross-sections, 1.08. The maximum stress intensity for the reduced cross-section is 46.60 ksi (43.15 x 1.08), which is less than the allowable value of 75.51 ksi for ASME Code properties.

Thus, the functional requirement for the overall tube bundle is satisfied during and following the Level D Service Conditions.

Table 4-1
Summary of Maximum Seismic Membrane Stresses

a, c

Table 4-2
Summary of Maximum Seismic Bending Stresses

a, c

Table 4-3
Summary of LOCA Rarefaction Stresses
Shutdown Cooling Line Break
In-Plane Bending

a, c

Table 4-4
Summary of Interpolated Stresses for LOCA Rarefaction
Shutdown Cooling Line Break
In-Plane Bending

a, c

Table 4-5
Summary of Maximum Membrane Stresses
Pipe Break Shaking

a, c

Table 4-6
Summary of Maximum Bending Stresses
Pipe Break Shaking

a, c

Table 4-7
Summary of Combined LOCA Stresses

a, c

Table 4-8
Summary of Combined Membrane + Bending Stresses *
LOCA + DBE Loading Conditions

Top TSP

a,c

Table 4-9
Summary of Combined Membrane + Bending Stresses *
LOCA + DBE Loading Conditions

U-Bend Region

a,c

Table 4-10
Summary of Through-Wall Pressure Stresses

a,c

Table 4-11
Summary of Combined / Principal Stresses
LOCA+DBE Loading Conditions
Nominal Tube Geometry

U-Bend Region

a, c

Table 4-12
Summary of Combined / Principal Stresses
LOCA+DBE Loading Conditions
Nominal Tube Geometry

Top TSP

a, c

Table 4-13
Summary of Tube Stress Intensities
LOCA+DBE Loading Conditions
Nominal Tube Geometry

U-Bend Region

a, c

Table 4-14
Summary of Tube Stress Intensities
LOCA+DBE Loading Conditions
Nominal Tube Geometry

Top TSP

a, c

Table 4-15
Summary of Combined FLB Axial Stresses

a,c

Table 4-16
Summary of Combined Membrane + Bending Stresses
FLB + DBE Loading Conditions

Top TSP

a,c

Table 4-17
Summary of Combined Membrane + Bending Stresses
FLB + DBE Loading Conditions

U-Bend Region

a,c

[ ]

Table 4-18
Summary of Combined / Principal Stresses
FLB+DBE Loading Conditions
Nominal Tube Geometry

U-Bend Region

a, c

[ ]

Table 4-19
Summary of Combined / Principal Stresses
FLB+DBE Loading Conditions
Nominal Tube Geometry

Top TSP

a, c

Table 4-20
Summary of Tube Stress Intensities
FLB+DBE Loading Conditions
Nominal Tube Geometry

U-Bend Region

a, c

[ ]

Table 4-21
Summary of Tube Stress Intensities
FLB+DBE Loading Conditions
Nominal Tube Geometry

Top TSP

a, c

SECTION 5

TUBE EVALUATION

DEGRADED TUBE CONDITIONS

5.1 Analysis Overview

This section establishes the minimum wall requirement for the tubes and compares stresses in the degraded tube against the appropriate structural limits. Calculations are performed to establish the minimum wall requirements for uniform tube wear and for wear over limited axial extent at the tube support plate and AVB intersections. The degraded tube is also evaluated relative to requirements for margin to burst, collapse loads, and leak-before-break requirements.

5.2 Uniform Tube Wear

5.2.1 Minimum Wall Requirement

In accordance with the stress classification in Table 2-1, the tubes are subject to primary stress limits for both membrane and bending stresses. [

]a,c

For computing t_{min}, the pressure stress shown below is used. That is,

$$t_{min} = \frac{\Delta P_i \ R_i}{P_m - 0.5(\Delta P_i)}$$

where,

ΔP_i = through-wall pressure gradient

R_i = tube inside radius

P_m = allowable primary membrane stress intensity

Using the above formulation, calculations are performed to determine the minimum acceptable wall thickness for uniform wall thinning. A summary of the minimum required wall thicknesses is provided in Table 5-1.

5.2.2 Uniform Thinning Over Limited Axial Extent

For locations having uniform degradation over a limited axial distance, such as for AVB locations, a reduced t_{min} is established by accounting for the strengthening effect of the remainder of the tube in terms of burst strength capability. It has been documented in Reference 10 that tubing with degradation over a limited axial length has higher burst strength capability than tubing with an equivalent amount of degradation over an unlimited length. A ratio of the burst pressure for a degraded tube to the burst pressure for an undegraded tube is shown below,

$$\frac{\Delta P}{\Delta P^o} = (1 - h/t)^{\left[1 - e^{-0.13L/\sqrt{R_i(t-h)}}\right]}$$

where,

ΔP = thinned tube burst pressure
ΔP^o = unthinned tube burst pressure

h = depth of thinning
t = nominal wall thickness
R_i = inside tube radius
L = length of thinned region

Using this relationship, a ratio is obtained for burst pressure for a tube having an unlimited length of degradation, which is defined to be 1.5 inches in Reference 10, to the burst pressure for a tube having degradation over a limited length. The minimum required wall thickness is then scaled using this ratio.

Utilizing the relationship for locally-degraded regions, reduced t_{min} requirements are established for the TSP and AVB intersections. Degraded lengths of 0.75 inch and 1.125 inches are considered. The 0.75 inch length corresponds to the AVB intersections, and the 1.125 inch length corresponds to the tube support plates. Note that the AVB dimension on the side next to the tubes is 0.480 inch. The 0.75 inch length for the AVB's conservatively accounts for the angular orientation of the AVB's relative to the tubes. Calculations to determine the reduced t_{min} for AVB and TSP intersections are summarized in Table 5-2. A summary of the minimum required wall thicknesses is provided in Table 5-3.

5.2.3 Primary Stress Limit Evaluation for Degraded Section

The locally degraded tube must also be evaluated against the stress limit for primary membrane plus bending (in-plane) stress intensity in the U-bend region, namely the AVB intersections. The tube stresses at the degraded locations are calculated by scaling the stresses for the non-degraded tube by the ratio of the corresponding section properties of the nominal and locally degraded tubes. Of the several sets of conditions evaluated, the location of maximum stress intensity occurs in the U-bend at the tube / AVB interface for the FLB+DBE loading combination. The minimum value for t_{min} in Table 5-3 at the AVB intersections is 0.014 inch (0.026 inch wear depth). The corresponding ratios for A_{nom}/A_{min} and $(c/I_{min})/(c/I_{nom})$ are 1.1269 and 1.0075, respectively. The overall combined FLB+DBE stresses for the degraded tube are summarized in Table 5-4. The resulting tube stress intensities are summarized in Table 5-5. The maximum stress intensity has a value of 37.46 ksi.

Based on the values in Table 2-4, the shape factor K has a value of 1.3129 for two-sided AVB thinning with a 0.014 inch wall thickness (wear depth = 0.028 inch). The corresponding allowable membrane plus bending stress limit is 74.02 ksi. The maximum stress intensity of 37.46 ksi satisfies the applicable stress limit.

5.3 Margin to Burst Under Normal Operating ΔP_i

The fundamental premise of the R. G. 1.121 criteria is that all tubes should retain margins of safety against burst consistent with the safety factor margins implicit in the stress limit criteria of the ASME Code, Section III, as referenced in 10 CFR 50.55a, for all service level loadings. Satisfaction of these criteria means that all tubes have been determined to retain the required margin against gross failure or burst under normal plant operating conditions. In addition, all tubes have been determined to retain a margin of safety against gross failure or burst consistent with the margin of safety determined by the stress limits in NB-3225 of Section III of the ASME Code under postulated accidents concurrent with a safe shutdown earthquake.

Since the tube min-wall (t_{min}) values calculated in Section 5.2.1 are based on stress limit criteria consistent with the ASME Code Section III criteria, the required margin to burst is satisfied.

5.4 Tube Collapse Evaluation

In addition to the primary stress limits, there is an additional requirement that the degraded region of the tubing withstand the external pressure loading from LOCA without collapse with a margin consistent with the Code criterion for faulted loads. That is,

$$0.9\ P_c \geq \Delta P_o$$

where:

P_c = collapse pressure of the degraded tubing, and
ΔP_o = external pressure loading due to the secondary-to-primary pressure gradient

For verifying the integrity of the thinned tube, the maximum secondary-to-primary ΔP_o occurs during the LOCA event. The maximum secondary to primary pressure following the

LOCA event is conservatively estimated to be 900 psi. Hence, in accordance with the ASME Code criterion, the minimum required collapse pressure is 900 / 0.9 = 1000 psi.

The collapse pressure is significantly affected by tube ovality. A number of correlations using limit analysis theory have been developed to predict collapse strength of ovalized tubes. The analytical correlation shown below, provided in Reference 11, has been found to be quite accurate for the thermally-treated (or stress relieved) tubing, believed to be due to its less anisotropic yield properties, compared to that of as-manufactured tubing.

$$P_c = \frac{2S_y t}{\rho\left[2+e\left(1+\frac{8\rho}{3t}\right)+\frac{4e^2\rho}{3t}\right]}$$

where,

$\rho = R_m$,

S_y = tube yield strength,

t = tube wall thickness,

and,

e = tube ovality

The validity and conservatism of this analytical correlation was verified against the results of room temperature collapse pressure tests on mill-annealed 0.75 inch OD x 0.043 inch t, and 0.875 inch OD x 0.050 inch t oval tubes. The comparison of analytically predicted (normalized) collapse pressures with those obtained from the tests is shown in Figure 5-1.

A summary of calculations to determine collapse pressure as a function of tube ovality for a minimum wall thickness of [ ]^{a,c} inch is provided in Table 5-6, and shown plotted in Figure 5-2. The maximum specified ovality ($OD_{max} - OD_{min}$) / OD_{nom} in the straight region of the tubing is 1.45 %. Corresponding to this ovality, the predicted tube collapse pressure for the reduced tube cross-section is [ ]^{a,c} psi. Since the expected collapse pressure is higher than the required minimum of 1000 psi, the minimum tube wall thickness of [ ]^{a,c} inch is acceptable.

In order to evaluate the potential for collapse at the AVB locations, data from collapse tests performed for tubes with machined flats similar to AVB wear, documented in Reference 12, is used. The tests utilized thirteen 7/8 inch x .050 inch thickness tubes with simulated penetrations in three basic configurations (A, Bl, and B2) as shown in Figures 5-3 and 5-4. The difference between the B1 and B2 configurations is the depth of thinning, with the B1 configuration being thinned by 75% (25% remaining wall), and the B2 configuration being thinned by 50% (50% remaining wall).

The test arrangement allowed the installation of a check valve that would allow controlled collapse so that local collapse could be determined. The upper end of the tube was attached to the check valve that was open to the atmosphere through a small hole in the vessel head. A sudden high velocity jet of water from this hole easily detected local collapse. Simultaneous pressure readings were taken from a panel-mounted gage. Wall thicknesses were determined indirectly by taking external micrometer measurements across the flats (or flat) and the round portion of the tube.

The test results are summarized in Table 5-7. A plot of collapse pressure as a function of percent thinning is provided in Figure 5-5. The results fall essentially into two general classes. The first class is a very local collapse of the flat thinned section only, and the second case is a more general "total local" collapse which involves the entire tube circumference in the vicinity of the milled flat. It is the case of total collapse that is of interest here, as a small local collapse immediately adjacent to the wear scar will not result in a significant reduction in flow area for the tube. The data corresponding to the total collapse case can be approximated using the following exponential curve.

$$y = a e^{bx} ,$$

where,

y = Collapse pressure – psi
a = [ ]a,c
b = [ ]a,c
x = Percent Thinning

A plot showing the above curve fit to the data is shown in Figure 5-6. By adjusting the curve downward [ ]a,c psi, a lower bound curve approximating the collapse pressure for two-sided AVB wear is obtained. The lower bound curve and corresponding exponential expression are also shown on Figure 5-6. Numerous experimental studies have shown that the collapse characteristics of thick-walled tubes are directly related to the R_m / t ratio of the tube. Comparison of the R_m / t ratios for [ ]a,c tubes shows them to be nearly identical, 8.25 versus 8.09, respectively. Thus the test data for the two-sided wear is judged to be applicable to the ANO replacement steam generator tubes.

Using the lower bound expression, the collapse pressure for two-sided AVB wear with a wear depth of [ ]a,c psi. Using the same expression, the collapse pressure for a nominal tube is [ ]a,c psi. Thus the worn tube has a predicted collapse pressure that is equal to [ ]a,c of the collapse pressure for a nominal tube.

Using the algorithm for collapse pressure as a function of tube ovality, calculations to determine collapse pressure for a 0.688 x 0.040 inch tube as a function of tube ovality are provided in Table 5-8, and shown plotted in Figure 5-7. The maximum specified ovality in the U-bend region of the tubing is 2.8%. Corresponding to this ovality, the predicted tube collapse pressure is [ ]^{a,c} psi. Applying the above ratio for collapse under two-sided AVB wear results in an estimated collapse pressure of [ ]^{a,c} psi. Since the expected collapse pressure of [ ]^{a,c} psi is higher than the required minimum of 1000 psi, the minimum tube wall thickness of [ ]^{a,c} inch at the AVB intersections is acceptable.

5.5 Tube Leakage Limits

The rationale behind the limitation on tube leakage is to limit the maximum allowable (primary-to-secondary) leak rate during normal operation such that the associated crack length (through which the leakage occurs) is less than the critical crack length corresponding to the maximum postulated accident condition pressure loading. Thus, on the basis of leakage monitoring during normal operation, unstable crack growth is not expected to occur in the unlikely event of the limiting accident.

Burst pressure is often presented in the form of a relationship between a normalized burst pressure, P_N, and a normalized crack length, λ. The normalized burst pressure is simply the actual burst pressure non-dimensionalized by the flow stress of the material, and adjusted for the size of the tubing by the ratio of the mean radius to the thickness. This provides a ratio of a membrane stress in the tube to the strength of the material, and allows for the correlation to be applicable to multiple tube sizes. The flow stress of the material is usually taken as a linear function of the yield stress, S_y, and the ultimate tensile stress, S_u, of the material. Acceptable correlations for Alloy 600 tubes have been obtained using one-half of the sum of the two properties as the flow stress.[2]

For a tube with a mean radius of R_m and a thickness t, the normalized burst pressure as a function of the actual burst pressure, P_B, is defined as

$$P_N = \frac{P_B R_m}{(S_y + S_u)t}.$$

The normalizing parameter, λ, for the crack length, a, is defined as

$$\lambda = \frac{a}{\sqrt{R_m t}}$$

a form which arises in theoretical solutions to the burst problem. The burst pressure as a function of axial crack length for a specific tube size is then easily obtained from the non-dimensionalized relationship.

Historically, the relationships presented for correlating the burst pressure to axial crack length for Alloy 600 tubing are based on empirical data. Until recently, one common

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2 See Footnote 1, Page 1-2.

method of testing consisted of internally pressurizing an axially cracked (or slitted by electrical discharge machining) tube that had been lined with a flexible neoprene or tygon tube, i.e., a bladder, until a burst occurred. Burst is considered to have occurred when the crack opens to the extent that the bladder extrudes, and may rupture, accompanied by ductile (plastic) tearing of the tube material at the ends of the crack. If the bladder has ruptured and tearing of the crack has not occurred, the test specimen is not considered to have truly ruptured. This simply means that the opening of the flanks of the crack was sufficient to permit extrusion of the bladder, and that the actual, or true, burst pressure was not achieved during the test.

Test specimens have consisted of tubes with cracks that have been extended by high cycle fatigue from a starting notch, either part way or all the way through the thickness, or which have very narrow axial slits machined in them. Typical slit widths are in the range of 6 to 10 mils. The accepted method of creating the starting notch or the slit is by electrical discharge machining (EDM). Testing has demonstrated that both types of specimens behave similarly, thus the added expense of fatigue extension of the EDM slit is generally not justified. In addition, testing is usually conducted at room temperature, with the results adjusted to operating temperature via the change in the flow stress of the material.

In contrast to the testing previously described, tube burst testing in Belgium and France typically included a thin foil shim on the outside of the bladder at the location of the crack or slot. The purpose of the shim was to provide a small reinforcement to prevent extrusion and rupture of the bladder before rupture of the tube. Shim dimensions are usually ~1/2 inch wide by ~6 mils thick with the length chosen to extend ~1/4 inch beyond each end of the slit. The shim material was typically brass, although stainless steel has also been used. Burst pressure results from those tests were typically higher than results obtained from similar tests with the bladder not reinforced.

To determine which methodology (with or without bladder reinforcement) produced results more representative of burst pressures that might be expected in operating SGs, several burst tests were performed at the Schelle fossil plant in Belgium. These tests utilized the large water supply and large pumping capacity of the plant to maintain and increase the pressure during the tests. Burst pressure data was obtained for thirteen 7/8 inch OD by 0.050 inch thick and two 3/4 inch OD by 0.043 inch thick Alloy 600 tube specimens with a variety of slot lengths without employing a bladder. The data obtained demonstrate burst strengths exceeding the results obtained with non-reinforced bladders. After additional review of the data, and comparisons with burst data for specimens where foil reinforcement was not used, it was thus judged that a 5% reduction in burst pressure should be applied to all test results in which foil reinforced bladders had been used.

A series of regression analyses, summarized in Reference 13, were performed for available burst data, considering a variety of linear and non-linear functions. An exponential function was finally selected based on the combination of maximizing the goodness of fit, minimizing the number of coefficients in the function, and the hypothesis that the burst pressure should be a monotonicly decreasing function of the crack length. The function that was concluded to provide the best fit of the burst data is,

$$P_N = 0.0613 + 0.536\, e^{-0.278\lambda}$$

A plot comparing the predicted normalized burst pressure as a function of normalized crack length to the corresponding test data is provided in Figure 5-8.

The resulting burst curve for ANO tubes is shown in Figure 5-9 with a tabular summary of the burst data provided in Table 5-9. It is observed that through-wall crack length of [ ]a,c inch is required for a FLB pressure gradient of [ ]a,c psi.

The largest permissible crack length based on leakage considerations is determined using a computer program CRACKFLO. Computer program CRACKFLO has been developed for predicting leak rates through axially oriented cracks in a steam generator tube. The CRACKFLO leakage model has been developed for single axial cracks and compared with leak rate test results from pulled tube and laboratory specimens. Fatigue crack and stress corrosion cracking (SCC) leakage data have been used to compare predicted and measured leak rates as shown in Figure 5-10. Generally good agreement is obtained between calculation and measurement with the spread of the data being somewhat greater for SCC cracks than for fatigue cracks.

A summary of the corresponding leak rates under normal operation as a function of crack length calculated using CRACKFLO is provided in Table 5-10. Leak rates are shown both for the mean data and for the lower 95% probability level. Calculations to determine the allowable leak rate for the crack length corresponding to burst under FLB conditions calculated above are summarized in Table 5-11. The leak rate corresponding to a crack length of [ ]a,c inch is shown to be [ ]a,c gpd.

The Technical Specification primary-to-secondary leak rate limit of 150 gpd for the ANO 2 replacement steam generators, which is the operational leakage performance criteria included in NEI-97-06, Rev. 1B (Reference 16), enhances the potential for leak-before-break during subsequent plant operation. Plant shutdown will commence if primary-to-secondary leakage exceeds 150 gpd in any one steam generator. The principal protection against tube rupture is provided by the safety margins inherit in the ASME Code stress limits. The 150 gpd limit provides further protection against tube rupture for a rogue tube that might experience crack growth at much greater than expected rates. The results above, showing the leak rate under full power conditions, corresponding to a crack length resulting in burst under an accident condition pressure of 2560 psi, to be [ ]a,c gpd, show the operational leakage performance criteria to be satisfied.

Table 5-1
Summary of Minimum Acceptable Wall Thickness (t_{min})
Unlimited Length of Degradation

a, c

Table 5-2
Calculations to Determine Allowable t_{min}
Uniform Thinning Over a Limited Length

a,c

Table 5-3
Summary of Minimum Acceptable Wall Thickness (t_{min})
Limited Length Degradation

a, c

Table 5-4
Summary of Combined / Principal Stresses
FLB + DBE Loading Conditions
Locally-Degraded Tube

U-Bend Region

a, c

Table 5-5
Summary of Tube Stress Intensities
FLB + DBE Loading Conditions
Locally-Degraded Tube

U-Bend Region

a, c

Table 5-6
Tube Collapse Pressure as a Function of Tube Ovality

t_{min} = 0.017 inch

a,c

Table 5-7
Collapse Pressures for Straight 7/8 – 0.05 Inconel Tube
With Simulated Wall Thinning

a,c

Table 5-8
Tube Collapse Pressure as a Function of Tube Ovality

t_{min} = 0.040 inch

a,c

Table 5-9
Burst Pressure Versus Crack Length

a, c

Table 5-10
Prediction of Leak Rates
Full Power Conditions

a,c

Table 5-11
Leak Rate Versus Crack Length

a,c

a, c

Figure 5-1
Correlation Between Tube Ovality and Collapse Pressure

a,c

Figure 5-2
Tube Collapse Pressure as a Function of Tube Ovality
t_{min} = 0.017 inch

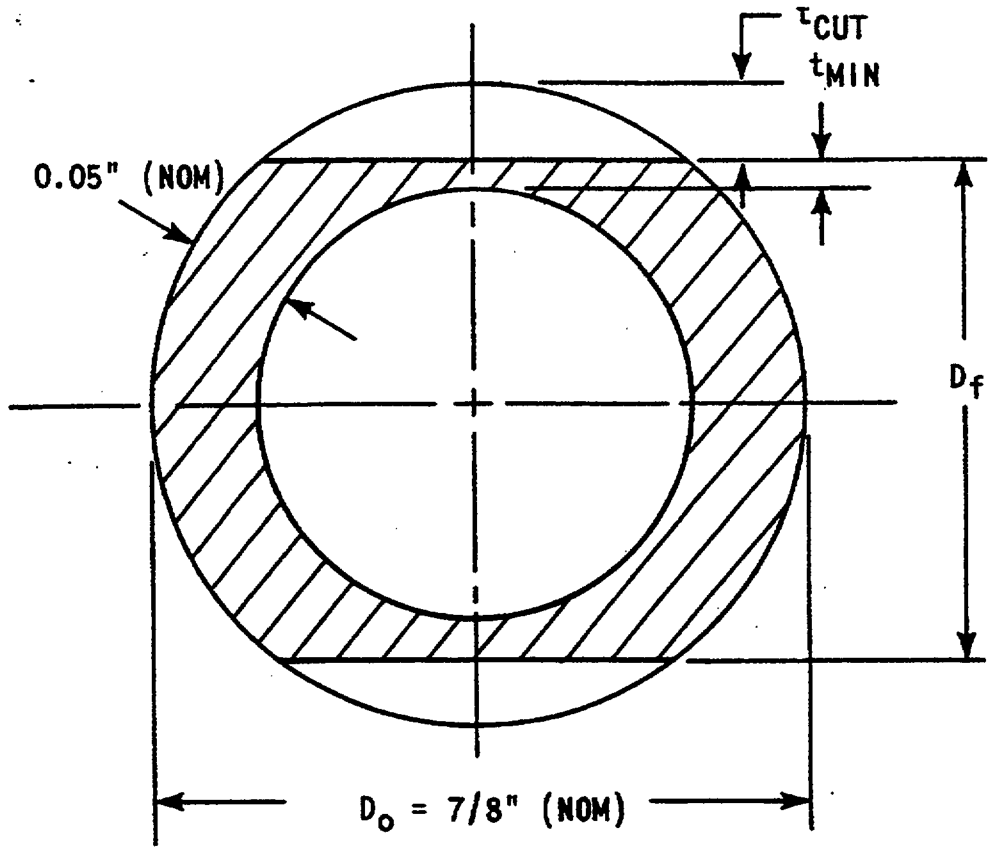

Figure 5-3
Thinned Tube Cross Section for Collapse Tests
Type A Configuration

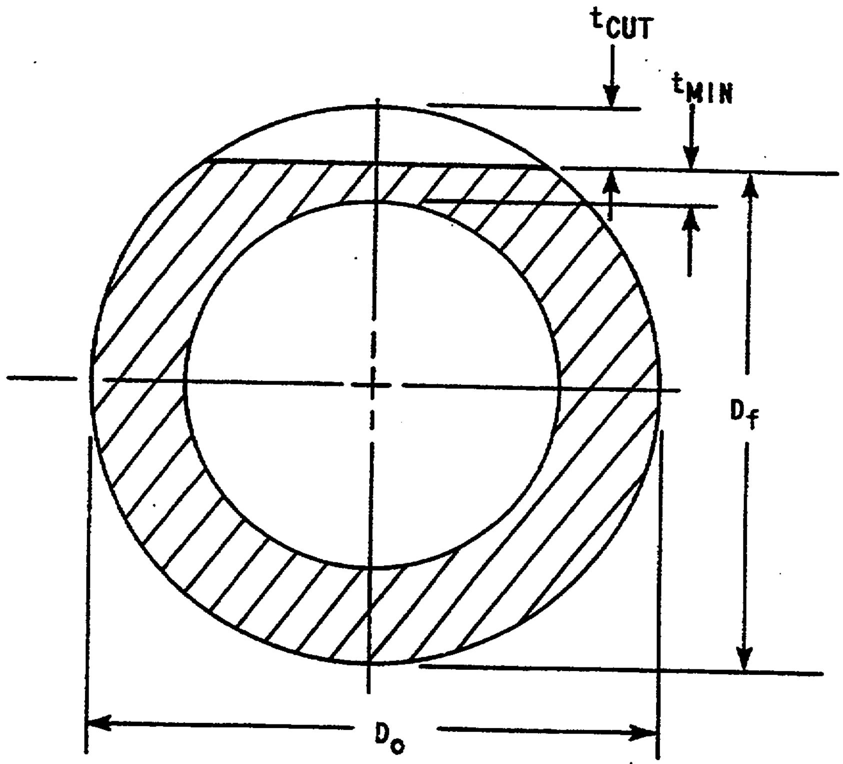

Figure 5-4
Thinned Tube Cross Section for Collapse Tests
Types B1 and B2 Configuration

a,c

Figure 5-5
Collapse Pressures for Straight 7/8 x 0.05 Inconel Tubes With Simulated Wall Thinning

a,c

[ ]

Figure 5-6
Collapse Pressures for Straight 7/8 x 0.05 Inconel Tubes
With Simulated Wall Thinning
Exponential Curve Fit of Total Collapse Data

a,c

Figure 5-7
Tube Collapse Pressure as a Function of Tube Ovality
t_{min} = 0.040 inch

a, c

Figure 5-8
Normalized Burst Pressure Versus Normalized Crack Length
Alloy 600 Steam Generator Tubes

a, c

Figure 5-9
Burst Pressure Versus Crack Length

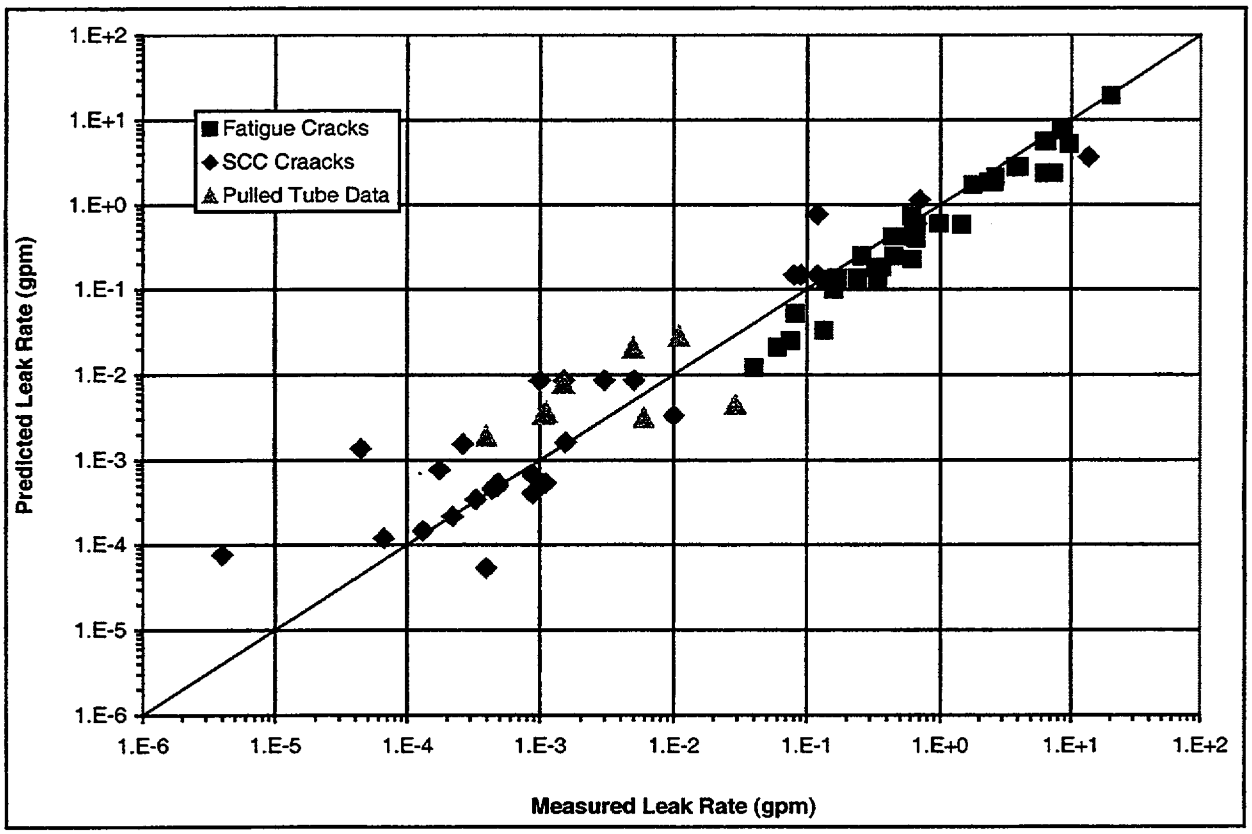

Figure 5-10
Comparison Between Predicted and Measured Leak Rates

SECTION 6

RECOMMENDED TUBE REPAIR LIMITS

The minimum acceptable wall thickness and other recommended practices in Regulatory Guide 1.121 are used to determine a repair limit for the tube. The Regulatory Guide was written to provide guidance for the determination of a repair limit for steam generator tubes undergoing localized tube wall degradation. Tubes that are determined to have indications of degradation in excess of the repair limit would have to be repaired or removed from service.

As recommended in paragraph C.2.b. of the Regulatory Guide, an additional thickness degradation allowance must be added to the minimum acceptable tube wall thickness to establish the operational tube thickness acceptable for continued service. Paragraph C.3.f. of the Regulatory Guide specifies that the basis used in setting the operational degradation allowance include the method and data used in predicting the continuing degradation and consideration of eddy current measurement errors and other significant eddy current testing parameters. A summary of the tube structural limits as determined by this analysis is provided in Table 6-1. The corresponding repair limits are established by subtracting from the structural limits an allowance for eddy current uncertainty and continued growth.

In the absence of an operational history for the ANO replacement steam generators, the allowance for continued degradation is based on past experience. Based on available experience with Alloy 690 tubing, it is concluded that tubes exhibit a low probability of experiencing cracking modes of degradation. Potential tube degradation, if it should occur, would likely be the result of tube wear. Past experience has shown tube wear to occur primarily at tube / AVB intersections. Additionally, operational experience has shown tube wear to be reduced to very low levels at tube / AVB intersections for Westinghouse designed steam generators with a U-bend design comparable to the configuration for the ANO replacement SG. In addition, results of a tube wear evaluation, documented in Reference 14, show that the maximum tube wear at an AVB location is [ ]^{a,c} mils over 40 years. This corresponds to [ ]^{a,c} % of the nominal tube wall over 40 years, or [ ]^{a,c} %/year. Thus, it is judged to be conservative to incorporate an allowance for continued degradation on the order of 5% of the tube wall thickness for continued degradation resulting from tube wear for one fuel cycle. Also, past experience has shown that characterization of the depth of wear for tube wear degradation modes using eddy current inspection techniques to be within a 10% allowance for uncertainty. Thus, for establishing the resulting tube repair limit, a 10% allowance for eddy current uncertainty will be implemented.

The applicable structural limits for the tubes are summarized in Table 6-1. Corresponding tube repair limits are calculated on the basis of a 5% allowance for continued degradation and an additional 10% for eddy current uncertainty. The resulting tube repair limits are also shown in Table 6-1.

Finally, the analysis results show the leak rate under full power conditions corresponding to a crack length resulting in burst under an accident condition pressure of 2560 psi to be [ ]a,c gpd. Thus, the Technical Specification primary-to-secondary leak rate limit of 150 gpd for the ANO 2 replacement steam generators, which is the operational leakage performance criteria included in NEI-97-06, Rev. 1B, is shown to be satisfied.

Table 6-1
Summary of Tube Structural and Repair Limits

a, c

SECTION 7

REFERENCES

1. "ASME Boiler and Pressure Vessel Code", Section III, 1989 Edition.

2. USNRC Regulatory Guide 1.83, Rev. 1, "In-Service Inspection of Pressurized Water Reactor Steam Generator Tubes", July 1975.

3. USNRC Regulatory Guide 1.121 , "Bases for Plugging Degraded PWR Steam Generator Tubes (for comment)," August 1976.

4. PWS 4.802 (SG-87-12-001), "Leak and Burst Results From Tests on Inconel 690 Tubing", B. C. Gowda, 12/87.

5. WCAP-12522, "Inconel Alloy 600 Tubing - Material Burst and Strength Properties," J. A. Begley, J. L. Houtman, January 1990.

6. Design Specification 413A80, Revision 2, "Entergy Operations Incorporated Arkansas Nuclear One Unit 2 Delta 109 Replacement Steam Generator", 4/14/00.

7. Report CENPD-133P and Supplements 2, 4 and 5, "CEFLASH-4A, A FORTRAN IV Digital Computer Program for Reactor Blowdown Analysis," June 1985.

8. EDRE-EMT-1401, "Engineering Services Abbreviated Y2K Assessment for WECAN/Plus Version 97", A. J. Kuenzel, 12/6/99.

9. WCAP-15280, "Arkansas Nuclear One Unit 2 Delta 109 Steam Generator Flow Induced Vibration and Tube Wear/Corrosion Evaluation", May, 2000.

10. NUREG/CR-0718, PNL-2937, "Steam Generator Tube Integrity Program Phase I Report", September, 1979.

11. ASME Paper No. 77-PVP-57, "Plastic Collapse of Oval Straight Tubes Under External Pressure", N. C. Small, 6/77.

12. WCAP-8429, "Analytical and Experimental Evaluation of the Mechanical Integrity of Steam Generator Tubing", 6/81.

13. Westinghouse Report, SG-95-03-010, "Burst Pressure Correlation for Steam Generator Tubes With Throughwall Axial Cracks", R. Keating, 3/95. (EPRI Proprietary)

14. WCAP-15280, "Delta 109 Arkansas Nuclear One Unit 2 Steam Generator Flow Induced Vibration and Tube Wear / Corrosion Evaluation", 5/2000.

15. ASME Code Section III – Code Case N-20-3, 11/30/98.

16. NEI 97-06, Rev. 1B, "Steam Generator Program Guidelines", 11/99.