| | | | | |
|---|---|---|---|---|
| Duke Energy Corporation | Oconee 1, 2, 3 | The B&W Owners Group | AmerGen Energy Company, LLC | TMI-1 |
| Entergy Operations, Inc. | ANO-1 | | FirstEnergy Nuclear Operating Company | D-B |
| Progress Energy, Florida | Crystal River 3 | | Framatome ANP, Inc. (FANP) | |

Working Together to Economically Provide Reliable and Safe Electrical Power


February 17, 2005
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Washington, D.C. 20555-0001

**Background Discussion Material for February 24-25, 2005 NRC Meeting Relating to Replacement for BAW-2374, Revision 1, "Evaluation of OTSG Thermal Loads During Hot Leg LOCA"**

The B&WOG is providing the attached background material for use in preparation for our discussions planned for February 24-25, 2005 at the NRC offices in Rockville, Md. The purpose of this meeting is to explain the B&WOG approach for compliance with 10CFR50.46 consistent with our expectations for minimal steam generator tube failure and no predicted fuel cladding rupture failure.

Attachment 1 contains the framework for the proposed replacement topical report. Attachment 2 is the steam generator tube sever summary. Attachment 3 is the source term for resulting dose summary. Mr. Drew Holland is requested to distribute this material to prospective NRC participants in preparation for the meeting.

We look forward to the opportunity to share our approach with the NRC Staff prior to submittal of our replacement for BAW-2374, Revision 1, which is planned for December 2005.

Please forward future correspondence relative to this report to the Framatome ANP Director of Regulatory Affairs.

Sincerely,

Jerald S. Holm, Director
Regulatory Affairs

Howard Crawford, Chairman
B&W Owners Group Steering Committee

cc: D. G. Holland
Project 693

D045


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## ATTACHMENT 1

**Proposed Framework for Replacement for Topical Report BAW-2374, Revision 1, "Evaluation of OTSG Thermal Loads During Hot Leg LOCA"**

The purpose of the proposed replacement topical is to expand the technical information in the additional areas that are required to be addressed in the B&WOG efforts to eliminate consideration of the design basis large break LOCA in the upper hot leg for determining steam generator tube loads. Earlier versions of the topical report addressed such considerations as leak before break and risk informed arguments for achieving this objective. During the most recent review it was determined that generic approval of the topical report was not possible because 10 CFR 50.46, long term cooling criteria, had not been adequately addressed. Furthermore additional information relative to dose consequences and compliance with 10 CFR 100 needed to be addressed.

This revision expands discussion in these areas to demonstrate compliance. The proposed table of contents for this revision of the topical is provided below. Sections 1, 2, and 3.1 to 3.4 are intended to be very similar to those of BAW-2374 Revision 1. Previous NRC comments on these sections will be reviewed and addressed in the revised text. Sections 3.5, 3.6, and 3.7 in the proposed table of contents will be new to this revision. Section 3.5 will address will address the long term cooling criterion in 10 CFR 50.46. Section 3.6 will address the potential for dose consequences above and beyond the existing LOCA dose analyses. Section 3.7 will address the waterhammer potential resulting from primary fluid leaking into and filling up the secondary side of the steam generator and associated piping.

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# Attachment 2

Summary White Paper
(DRAFT)

Calculation of Projected OTSG Tube Severs Resulting from Postulated LBLOCA

Prepared by:
Jim Begley and Darrell Costa

**1.1**

## NOMENCLATURE

The nomenclature and variable definitions used throughout this report are presented below.

| VARIABLE | DESCRIPTION |
|---|---|
| A | tube cross-sectional area (~ 0.0683 $in^2$) |
| $A_d$ | degraded area ($in^2$) = (%depth/100)*t*flaw extent |
| BWOG | B&W Owners Group |
| E | Young's Modulus of Elasticity for tube (~31E6 psi) |
| ECCS | Emergency Core Cooling System |
| EFPY | effective full power years |
| EOC | end of cycle |
| F | force – axial tube load (lbs) |
| ID | tube inside diameter (in) |
| IGA | intergranular attack |
| L | tube length (56 ft or 672 inches) |
| LBB | Leak Before Break |
| LBLOCA | Large Break Loss of Coolant Accident |
| NDE | nondestructive examination |
| OD | tube outside diameter (in) |
| OTSG | Once-Through Steam Generator |
| PDA | Percent Degraded Area |
| POD | probability of detection |
| $R_i$ | tube inside radius (in) |
| RFO | refueling outage |
| $R_m$ | tube mean radius (in) |
| SBLOCA | Small Break Loss of Coolant Accident |
| SG | steam generator |
| $S_y$ | tube yield strength (psi) |
| $S_u$ | tube ultimate strength (psi) |
| t | wall thickness (in) |
| tw | through-wall |
| x | flaw extent (in) |
| $\Delta P$, DP | primary-to-secondary pressure differential (psi) |
| $\Delta T$, DT | tube-to-shell temperature differential (F) |
| $\delta$ | tube elongation, FL/AE (in) |
| $\varepsilon$ | tube strain ($\delta$/L) |

NOTE: Unless otherwise noted, all dimensions used in this report are in inches and all stresses and pressures are in psi.

# TABLE OF CONTENTS

## 1.0 INTRODUCTION:

Leak-Before-Break (LBB) criteria has been used for many years by the B&WOG utilities to eliminate the dynamic effects of Large Break Loss of Coolant Accidents (LBLOCA) from the design basis of their plants. Framatome ANP extended the LBB criteria to eliminate evaluation of the OTSG axial tube loads resulting from the thermal effects associated with rapid cooling of the SG tubes with emergency core cooling systems (ECCS) flows that follow a hot leg LBLOCA. Therefore, evaluations of the structural integrity of some parts of the steam generator and its repair hardware did not consider the potential large tube axial loads generated during this bounding postulated accident scenario.

Discussions with the NRC on use the LBB criteria for elimination of the thermal induced tube axial loads concluded that it may not have been appropriate because the thermal loads are not a dynamic loading that LBB review addressed. As a result, the B&WOG and Framatome ANP have prepared material (topical report) to justify not including the LBLOCA in the structural design basis of the steam generators. One part of the project is to define the potential tube loads associated with the event and the possibility of tube ruptures associated with those loads.

**The purpose of this document** is to provide an assessment of the potential effects of an LBLOCA on steam generator tube integrity. The summary includes discussions and results of calculations of axial tube loads, tube flaw structural limits, potential number of severed tubes, and associated leak rates resulting from a postulated LBLOCA. The basic items addressed within include:

1. description of the LBLOCA event and estimation of the resulting tube-to-shell temperature differences (ΔTs) and the primary-to-secondary pressure differences (ΔPs),
2. calculations of tube loads as a function of tubesheet radius and tube-to-shell ΔT,
3. calculations of tube flaw structural limits as a function of tubesheet radius and tube-to-shell ΔT,
4. calculations of probability of tube ruptures for the projected tube loads,
5. calculations of leak rates associated with LBLOCA loads and dilations, and
6. discussions of LBLOCA on condition monitoring, operability assessments and In Situ testing.

## 2.0 RESULTS:

Tube loads and flaw structural limits have been determined as a function of tubesheet radius and tube-to-shell ΔT. The tube loads are presented in Figure 5-2 and the allowable structural limits are provided in Figures 6-3 and 6-4.

Using a best estimate approach with existing plant inspection results and calculated tube loads, it is projected that no tubes will sever (0.02 probability of a single tube sever, 0.0002 probability of more than 1 tube sever) as a result of a LBLOCA event. By adding some margin of conservatism, one to three tubes could be assumed to rupture as a result of a LBLOCA event. Section 8.0 provides additional details and results.

## 3.0 ASSUMPTIONS:

There are no "key" assumptions used in this calculation that require verification.

## 4.0 DESIGN INPUTS:

Detailed structural thermal hydraulic and loading analyses of the postulated LBLOCA have not been performed. However, a detailed assessment of potential breaks in the RCS piping and an estimate of bounding tube-to-shell ΔT and associated primary-to-secondary ΔP have been provided. The assessment is provided in Appendix A of BAW-2374. Based on those discussions, the following inputs are used.

1. The maximum tube-to-shell ΔT is estimated to be 370F. The estimated ΔT is based on the extrapolation of RELAP5/MOD2-B&W data from Appendix A of BAW-2374.

2. The maximum primary-to-secondary ΔP associated with the maximum ΔT is estimated to be 45 psi and is based on the same RELAP5/MOD2-B&W data.

## 5.0 LBLOCA TUBE LOADS:

As previously stated, the detailed structural thermal hydraulics analysis of the LBLOCA has not been completed. As a result, the pertinent time history data needed to perform the detailed load analysis is not available. However, tube axial loads can be estimated by making use of the existing Small Break Loss of Coolant Accident (SBLOCA) results and the difference between the projected LBLOCA ΔT/ΔP and the SBLOCA ΔT/ΔP.

The SBLOCA transient is similar to the LBLOCA event in that the primary side blows down in both transients, the primary side refills with cool ECCS fluid, the primary and secondary pressures are both significantly reduced, and the differential pressure between the primary and secondary sides is very small. Therefore, the SBLOCA provides a solid basis for estimating LBLOCA loads.

In addition to the estimated LBLOCA loads, a maximum limiting load based on the actual material yield strength of the OTSG tubes is determined. The discussion in Section 5.3 provides evidence that the tube load is limited by the yield strength of the tube material. Based on the certified material test reports of the tube material used in constructing the OTSGs, a limiting load is determined.

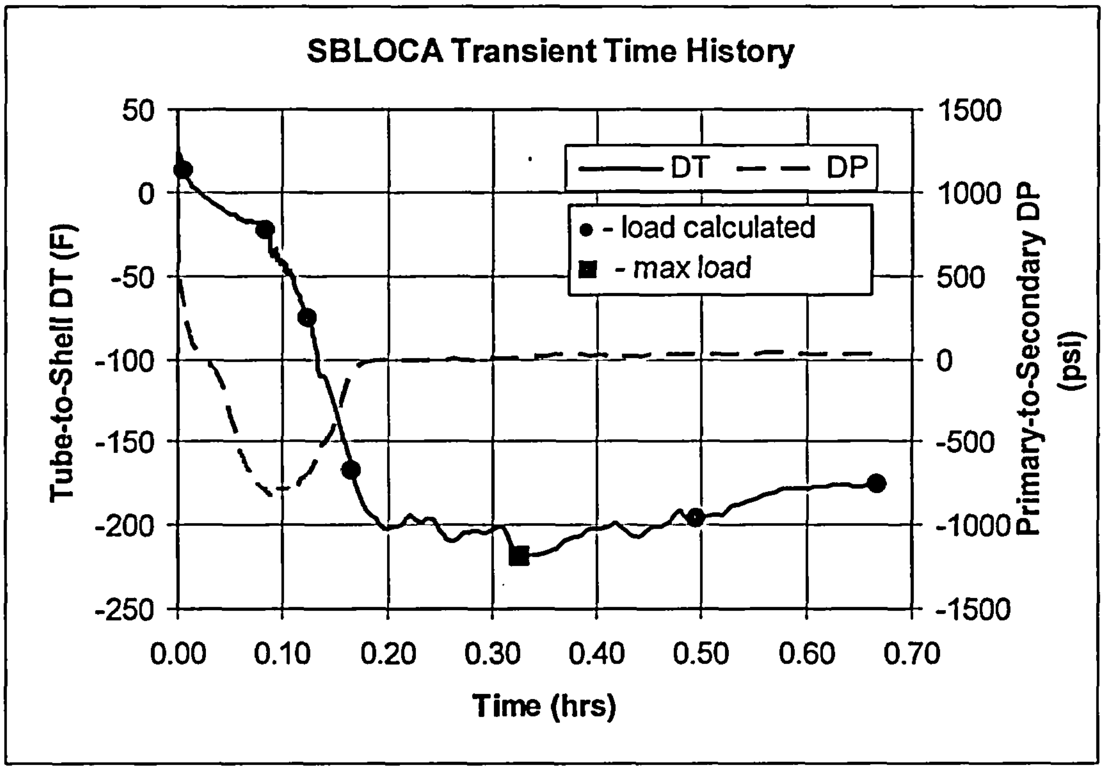
## 5.1 DESCRIPTION OF SBLOCA EVENT:

It has been determined that the limiting SBLOCA event is a result of the postulated rupture of the pressurizer surge line. The event results in a blow down (loss of pressure) of the reactor coolant system (RCS) followed by a refill with cold water from the ECCS. The ECCS cold water refill results in a much faster cooling of the thin tubes relative to the much thicker steam generator shell. The difference between the tube and shell temperatures creates the potentially large axial tensile loads in the tubes.

Thermal and structural analysis of an SBLOCA event for the two OTSG designed plant configurations, the standard lowered-loop plant and the raised-loop plant of Davis Besse, have been previously prepared. The time history of the SBLOCA for the two configurations is similar. A summary of the time history for the raised-loop plant is shown in Figure 5-1.

Figure 5-1 shows a sudden decrease in RCS pressure (~$\Delta P$ = -800 psi) followed by a decrease in the SG secondary side pressure (~$\Delta P$ = 0 to 50 psi). As the pressure reduces the tube-to-shell $\Delta T$ increases until a maximum value of ~220F is reached at about 0.32 hrs. The figure shows several times at which the tube axial loads were calculated and the time of maximum axial tube load (coincides with time of max $\Delta T$). Based on this information it is concluded that the time history is not a significant contributor and the use of the maximum tube-to-shell $\Delta T$ is acceptable for use in the calculation of LBLOCA tube loads.

Figure 5-1: Typical SBLOCA ΔT/ΔP Time History




## 5.2 LBLOCA TUBE LOADS

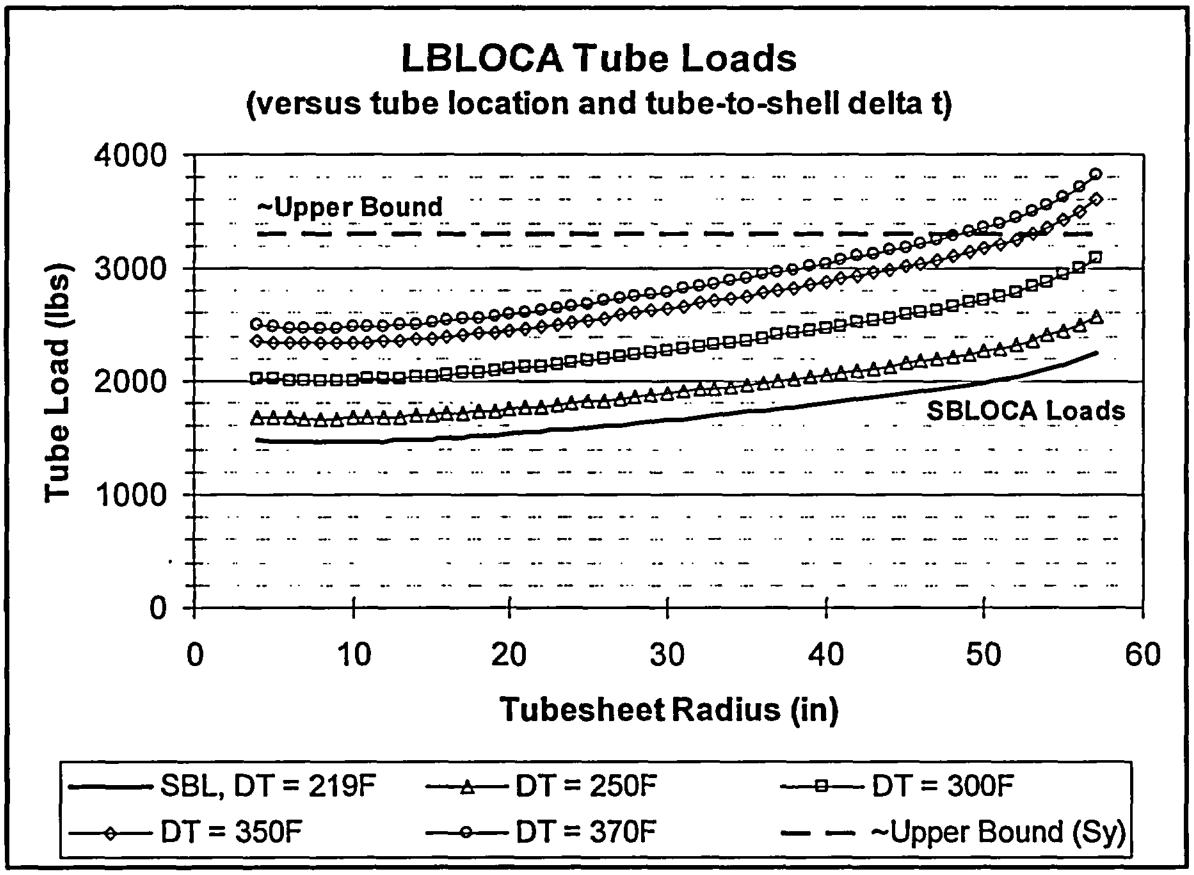
Calculations of the OTSG tube axial loads for the SBLOCA transient for both the lowered loop plants and the raised loop plant (Davis Besse) have also been prepared previously. The analysis evaluated both 0% and 25% tube plugging conditions for both SG configurations. A review of the resulting tube loads shows that the loads for all the cases are similar with the maximum loads occurring for the raised loop design with 0% plugging. The maximum axial load is 2257 lbs at the tube-to-shell ΔT of 219F.

The tube loads associated with the LBLOCA event will be determined by increasing the loads from the SBLOCA event by the ratio of LBLOCA ΔT to the SBLOCA ΔT. The ratio of loads by ΔTs is considered acceptable because nearly all the tube load for the two events is caused by the difference in temperature between the tubes and shell. The tube load due to preload for the two events is basically identical and is only about 60 lbs. The load contribution due to pressure at the time of maximum tube load (ΔT) is also nearly identical and practically negligible (~10 lbs) due to the small ΔP (< 50 psi). Therefore, the calculation of LBLOCA tube loads based on the ratio tube-to-shell ΔTs is reasonable and appropriate.

LBLOCA load = SBLOCA load x (LBLOCA ΔT / SBLOCA ΔT)

From Section 4, the maximum projected tube-to-shell ΔT for an LBLOCA event is estimated to be 370F degrees. The LBLOCA load associated with this maximum ΔT, as well as other smaller ΔTs are provided in Figure 5-2.

Figure 5-2: Tube Axial Loads




The figure provides the tube load distribution for the 219F ΔT SBLOCA transient and for projected LBLOCA ΔT cases. The load versus radius curves are based on elastic analysis results with no consideration of potential yielding of the tube. The dashed "upper bound" line represents the limiting load a SG tube can experience based on the yield strength of the tube. The following section provides the technical basis for the limiting the load to yield strength of the tube.

## 5.3 UPPER BOUND TUBE LOAD BASED ON TUBE YIELD STRENGTH

The following discussion provides the technical basis for limiting the tube axial load in the steam generators to a value necessary to yield the unflawed tube section ($F = S_y * A$). The yield limiting load is applicable to all transient conditions (including larger tube-to-shell $\Delta$Ts) and has been used in numerous documents.

The yield load theory is based on the fact that the axial load in the tube is a direct result of the difference in thermal expansion (axial elongation) of the SG shell and SG tube. Therefore, if the tube begins to yield, the tube elongation (strain) would continue only to the point that a balance of the thermal expansion (elongation, load) of the tube(s) and shell is reached. A calculation of absolute worse case limiting tube strain (elongation due to free thermal expansion of the shell) and comparison of results to a typical tube material stress-strain curve reveals that there is not sufficient elongation to create a significant amount of strain hardening in the tube and therefore the load is limited to the tube yield load.

OTSG Tube Yield and Ultimate Strengths:

A summary of room temperature material strengths from the certified material test reports for the tubes installed in the B&W steam generators is given in Table 5-1.

The tube temperature for the LBLOCA event is assumed to equal 150F. The value is based on the shell temperature of approximately 500F from the maximum $\Delta$T case for the SBLOCA event less the maximum postulated LBLOCA tube-to-shell $\Delta$T of 370F (500 - 370 = 130 or ~150F).

Table 5-1: Tube Material Strength

| | Temp (°F) | Yield Strength, $S_y$ (psi) | Ultimate Strength, $S_u$ (psi) |
|---|---|---|---|
| Average Strength | Room Temp | 49,587 | 99,768 |
| | 150F | 48749 | 99,149 |
| 150F assumed LBLOCA tube temperature | | | |

OTSG Tube Yield Limiting Loads:

Using the 150F temperature yield stresses and the OTSG tube nominal cross-sectional area of 0.0683 in$^2$ (0.625" OD by 0.037" wall) results in limiting tube loads of:

Avg yield load = 48749 psi * 0.0683 in$^2$ ~ 3300 lbs

OTSG Tube Elongations and Strains:

The tube elongation and strain associated with the elastically calculated tube load are:

Tube Elongation ($\delta$)= FL/AE

where: F = axial tube load, lbs
L = length of tube = 56 ft or 672 inches
A = tube area = 0.0683 $in^2$ (based on nominal tube wall)
E = Young's Modulus = ~ 31E6 psi

Tube Strain ($\varepsilon$) = $\delta$/L

where: $\delta$ = tube elongation, inches
L = length of tube = 672 inches

for 3300 lb load, (avg yield load)
$\delta = 3300(672)/(0.0683 * 31E6) = 1.05$ inches
$\varepsilon = 1.05/672 = 0.0016$ in/in = 0.16% strain

Tube Yield Load Summary and Conclusion:

Considering a typical stress-strain curve for alloy 600 material and the relatively small strains, the tube axial load is limited to approximately the yield load of the material. The remaining strain above that required to yield the material is extremely small and results in a negligible increase in load above the yield load. In addition, since the load is a result of thermal expansion, yielding of the tubes would decrease the tube load. Since the strains are so small and relatively close to the typical yield strain for the tube, it is not possible to achieve the full elastically calculated load. It is therefore concluded that the maximum thermally induced load for the OTSG tube is limited by the yield strength of the material.

## 6.0 LBLOCA - TUBE STRUCTURAL LIMITS:

Based on the transient descriptions for the LBLOCA (and SBLOCA) events, the critical loading from the event is the tube axial load associated with the large tube-to-shell $\Delta T$. Experience shows the only tube flaws affected by tube axial loads are those with circumferential extent. Therefore, only circumferential degradation need be considered in the assessment of tube structural integrity for the postulated LBLOCA event. The assessment will consider circumferential degradation located within the tube sheet and in the freespan.

The assessment of LBLOCA tube integrity begins by determining the allowable circumferential degradation (flaw size) as a function of tube axial load. Equations for tube rupture containing circumferential degradation confined to the tubesheet and for those in the freespan are used with a range of axial loads to develop curves of circumferential degradation versus load. The resulting curves are provided in Section 6.1.

The projected LBLOCA tube loads as a function of tubesheet radius and various magnitudes to tube-to-shell $\Delta T$ from Section 5.2 are then used with the allowable degradation versus axial load curves of Section 6.1 to determine the LBLOCA allowable circumferential flaw size as function of tubesheet radius and $\Delta T$. The results are presented in tables and figures of Section 6.2.

## 6.1 CIRCUMFERENTIAL DEGRADATION VERSUS TUBE AXIAL LOAD

The allowable axial loads based on the structural limits for circumferential degradation (PDA) are determined for flaws located in either in the tubesheet or in the freespan. The equations used for calculating the allowable loads are based on those provided in the EPRI Flaw Handbook. For flaws confined to the tubesheet, equation 5-25 from the Flaw Handbook is used. For flaws in the freespan, a slightly modified version of the Flaw Handbook equation is used. The FANP modified equation removes conservatism from the Handbook equation by using actual results from additional testing of tubes containing circumferential degradation. The modified equation has been used in plant specific CMOA evaluations. Both equations are based on degradation on the tube outer diameter (OD). An additional factor to account for the presence of pressure on the face of a flaw on the tube inner diameter (ID) is also provided in the Flaw Handbook. However, since the pressure associated with the LBLOCA axial load is negligible (<50 psi), no adjustment is needed and the equation is suitable for both OD and ID flaws.

**Tubesheet Flaw:**

The equation relating allowable axial load as a function of circumferential degradation for a flaw located within the tubesheet is:

$$F = [(S_y+S_u)(\pi)(R_m)(t)][(1.2128)(R_m/R_i)^2(1-k)]$$

Where: $F$ = tube axial load (lbs)

$S_y$ = tube yield strength (psi)
$S_u$ = tube ultimate strength (psi)
$R_m$ = tube mean radius (in)
$t$ = tube wall thickness (in)
$R_i$ = tube inside radius (in)
$k$ = PDA/100
PDA = percent degraded area = $100(A_d/A)$
$A_d$ = degraded area (in$^2$) = $(\%tw/100)*t*x$
$A$ = tube area (in$^2$) = $\sim t*2\pi*R_m$
$x$ = flaw extent, length (in)
flaw angle = $(360*x)/(2\pi R_m)$

The equation is solved for assumed magnitudes of PDA/100 from 0.000 to 1.000 in increments of 0.002. Other values used in the equation include:

$S_y$ = 48749 psi (avg.) @ 150F — Table 5-1
$S_u$ = 99149 psi (avg.) @ 150F — Table 5-1
$R_m$ = (0.625-0.037)/2 = 0.294 in
t = 0.037 in
$R_i$ = 0.551 in

The results of the evaluation are provided in Figure 6-1 as the straight dashed line identified as equation 1.

**Freespan Flaw:**

The allowable load for circumferential degradation in the freespan is actually based on the minimum of two calculated values. The first is identical to the tubesheet flaw and is based on pure tensile loading. The second equation accounts for the bending associated with the non-concentric loading of a flawed tube under axial load. The equation relating the allowable axial load as a function of circumferential degradation is:

$$F = [(S_y+S_u)(\pi)(R_m)(t)][ak^6-bk^5+ck^4-dk^3+ek^2-fk+g]$$

The parameters "a" to "f" are FANP proprietary constants. The other parameters and magnitudes used for this equation are the same as those defined above for tubes with degradation located within the tubesheet.

The results of the calculations are provided in Figure 6-1 as the curved line identified as equation 2. The limiting of the two equations is also shown in Figure 6-1 and provides the limiting allowable axial load for freespan circumferential degradation in a tube under axial loading.

In addition to determining the allowable axial load versus PDA, the allowable circumferential flaw extent for a freespan flaw versus axial load is determined for various flaw depths (40%, 60%, 80% and 100%). The results of the calculations are provided in Figure 6-2.

Figure 6-1: Allowable Axial Load vs Circumferential Flaw PDA

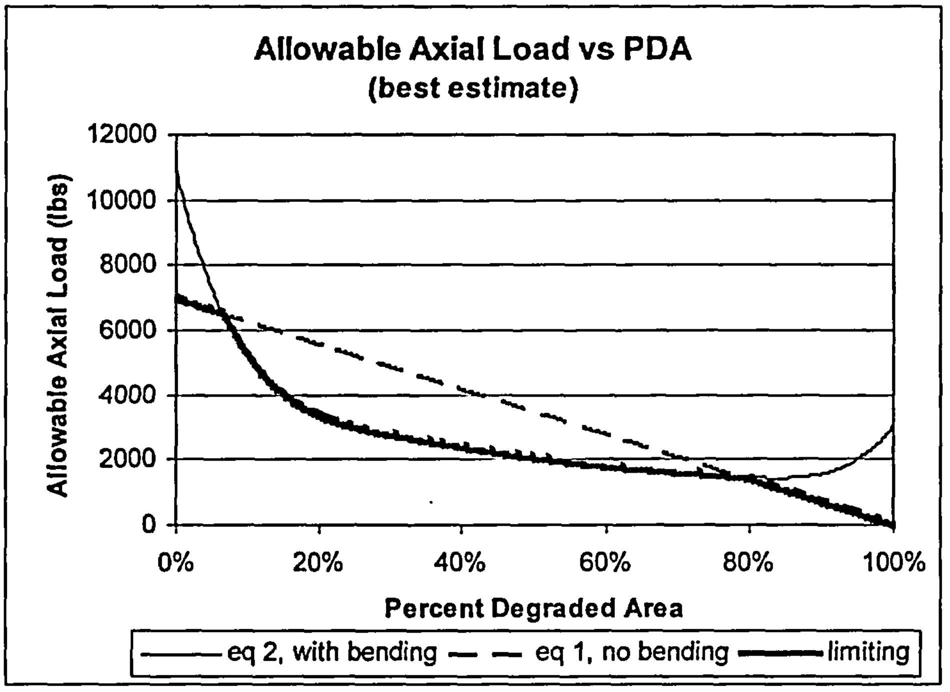
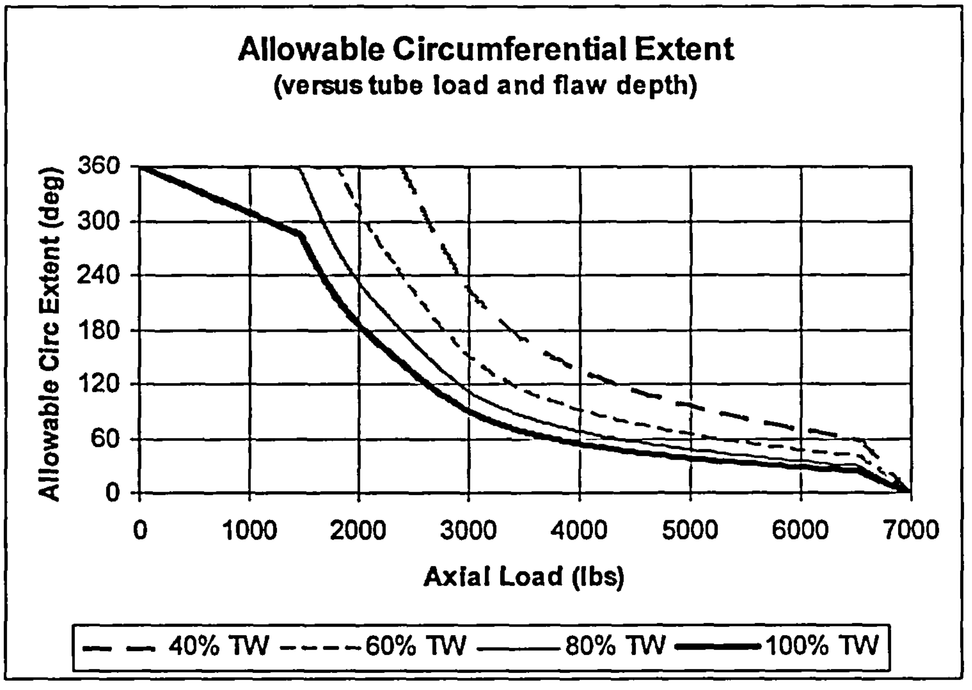



Figure 6-2: Freespan Allowable Circumferential Extent vs Axial Load & TW%

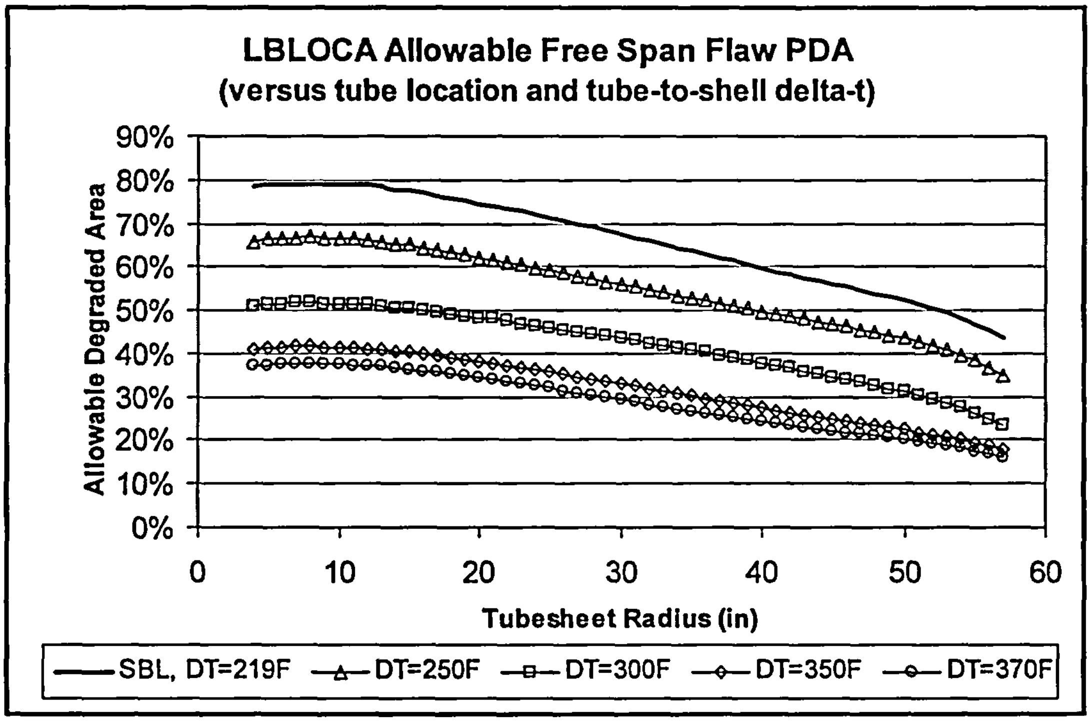


## 6.2 LBLOCA CIRCUMFERENTIAL DEGRADATION STRUCTURAL LIMITS

The allowable axial load vs PDA from Section 6.1 is used with the LBLOCA tube loads from Section 5.2 to determine the LBLOCA allowable circumferential degradation as a function of tubesheet radius and tube-to-shell ΔT. An EXCEL spreadsheet and table "lookup" option was used to find the allowable degradation for the defined LBLOCA loads. The results for both tubesheet and freespan degradation are provided in Figure 6-3.

In addition to the allowable PDA versus tubesheet radius values, the allowable circumferential extent for a 60% TW flaw was determined as a function of tubesheet radius and tube-to-shell ΔT. The allowable extent was determined by taking the allowable PDA from Figure 6-3 and dividing by the assumed through-wall flaw depth and then multiplying by 360 to get degrees. The results for both tubesheet and freespan degradation are provided in Figure 6-4.

Figure 6-3: Allowable Circumferential Flaw PDA
(free span and tubesheet flaws)



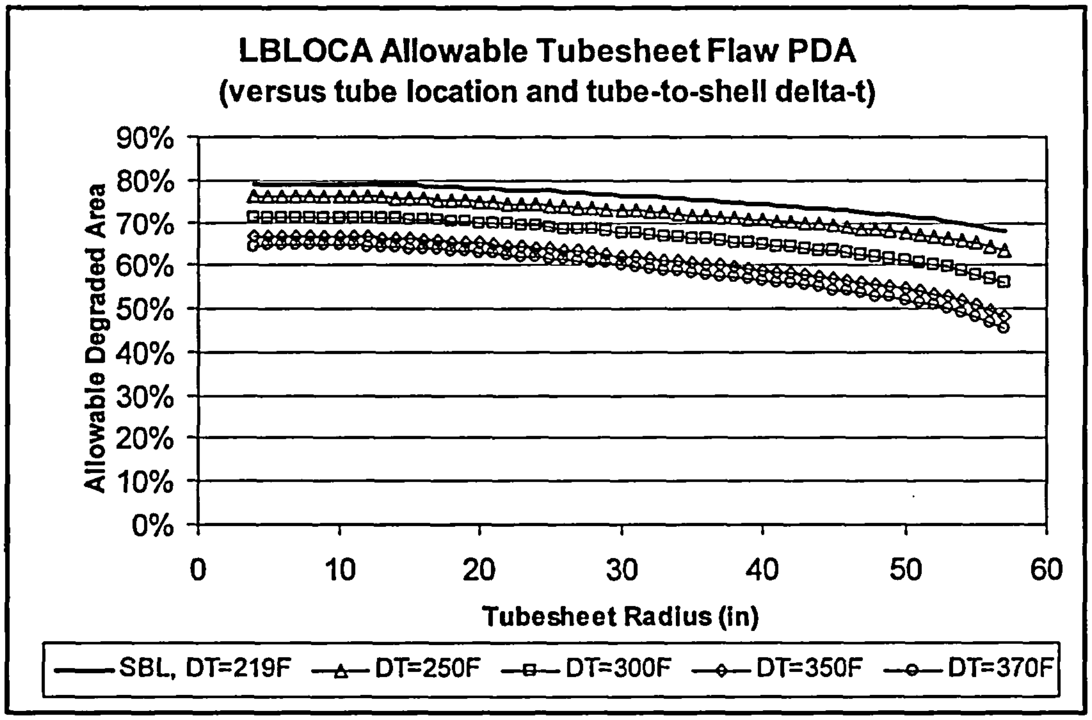
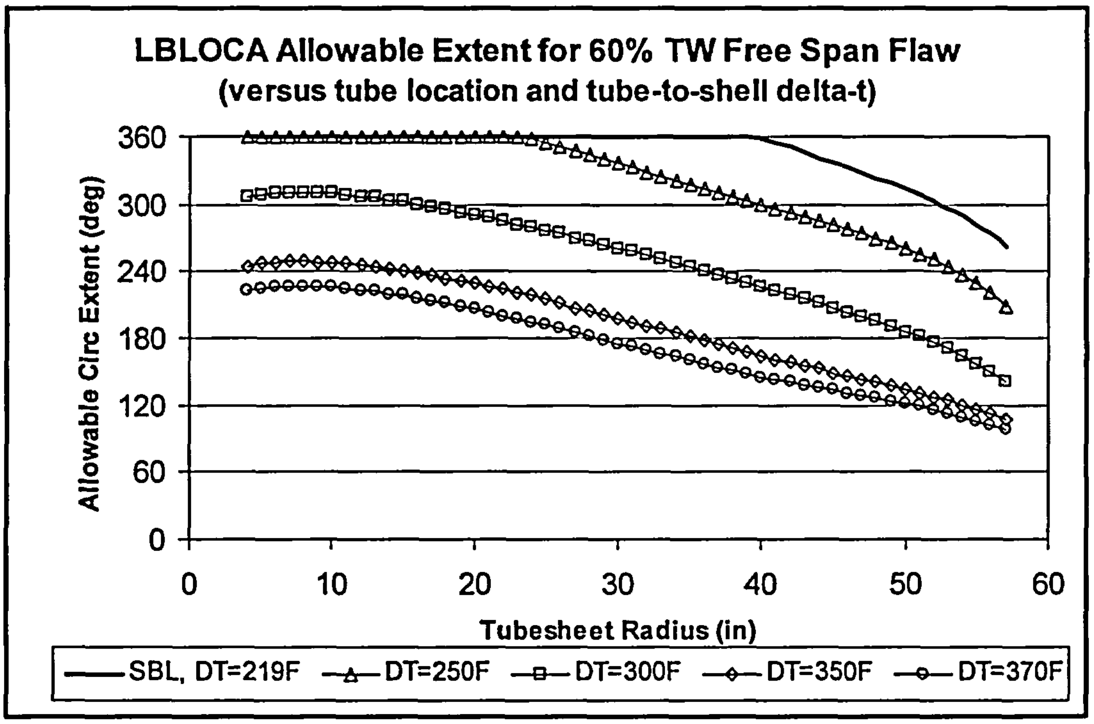
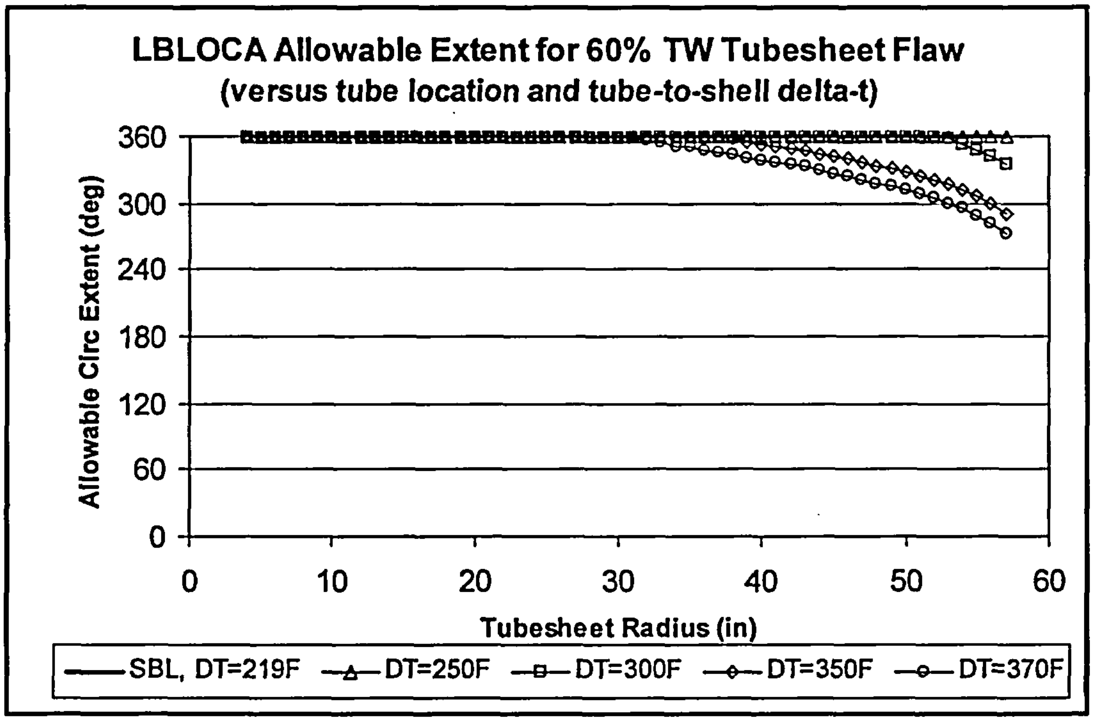



Figure 6-4: Allowable Circumferential Extent for 60% TW Flaw
(free span and tubesheet flaws)



## 7.0 PROJECTED LBLOCA LEAKAGE

Axial loads are the only significant loads of interest during an LBLOCA event. Axial degradation is not significantly influenced by axial loads. Only circumferential cracking and volumetric degradation needs to be considered. In terms of axial strength, the circumferential extent and depth of degradation are the parameters of interest for volumetric flaws as is the case for circumferential cracking. The limiting case for the axial strength of volumetric flaws is a circumferential crack of the same circumferential extent and depth

## 7.1 DEGRADATION LOCATIONS

Figure 7-1 shows a sketch of an OTSG tube. Circumferential cracking has been observed at the following locations:

- Upper and Lower Tube Ends
- Upper Tubesheet Roll Transitions
- Upper and Lower Tubesheet Secondary Faces
- Upper Tubesheet Crevices
- Freespan Dents and Dings

Volumetric Intergranular Attack (IGA) has been observed in upper and lower tubesheet crevices and in the freespan. There are several instances of chemistry transients leading to volumetric IGA. Three Mile Island Unit 1 (TMI-1) had a primary side chemistry transient during layup leading to ID IGA in the upper tubesheet crevice region. Secondary side chemistry transients have led to volumetric OD IGA in the first span at Crystal River Unite 3 (CR-3) and in the upper tubesheet crevice at Arkansas Nuclear One Unit 1 (ANO-1). NRC approved alternate repair criteria (ARC) have been applied at these plants which allows some IGA degraded tubes to remain in service. Davis Besse has experienced a secondary side incursion of Lake Erie cooling water in the past. Although a plug on detection criteria has been applied, volumetric OD IGA continues to appear.

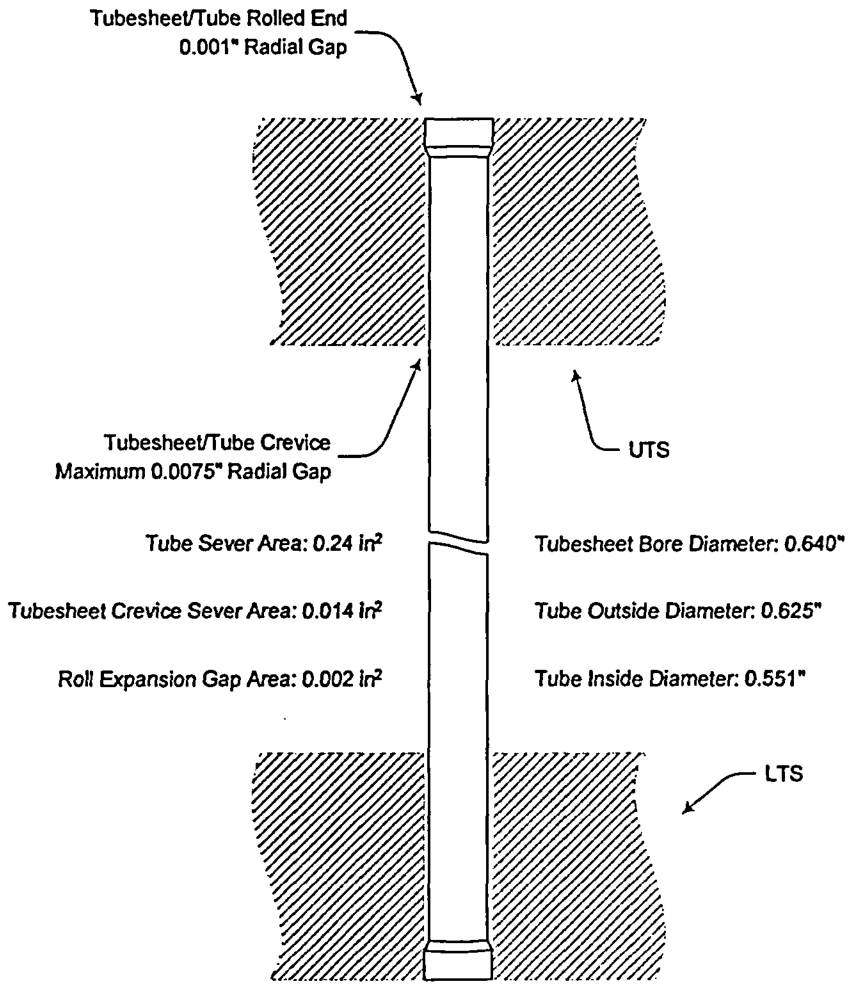



Figure 7-1: Schematic of an OSTG Steam Generator Tube

## 7.2 LEAKAGE AREAS

The leakage path for circumferential cracking at tube ends is the tube-to-tubesheet interface at the rolled expansion portion of the tube. Even considering maximum tube dilation effects this leakage area is less than 0.002 inches$^2$ (based on dilation difference between tubesheet bore and tube OD in expanded region of 0.002 inches. For degradation within tubesheet crevices the limiting leakage path is the gap between the unexpanded tube and the tubesheet. The width of this annular gap is 0.0075 inches leading to a maximum leakage area of 0.014 inches$^2$. These leakage areas are overwhelmed by the leakage area of a freespan tube sever. The ID area of a tube is 0.24 inches$^2$. For a double ended guillotine break, the leakage area is then 0.48 inches$^2$. From the perspective of large leak rates, only freespan tube severs are of interest. The LBLOCA leakage contribution from tube end and tubesheet crevice degradation is a small fraction of the leakage from a freespan tube sever.

The maximum LBLOCA axial load leads to a tube elongation of about 1 inch. A tube sever within 1 inch of a tubesheet secondary face would thus lead to a freespan leakage path. In considering the degradation that may lead to a freespan tube sever, the freespan region is taken conservatively as the region 3 inches above the upper tubesheet secondary face (UTS) to 3 inches below the lower tubesheet secondary face (LTS).

## 8.0 PROBABILITY OF FREESPAN TUBE SEVERS

This section deals with the probability of freespan tube severs. The axial strength of degradation is briefly summarized followed by an illustration of tube sever expectations from the past history of circumferential and volumetric degradation in OTSGs. The methodology of probability calculations is described followed by consideration of the length, depth, number and radial location of past and projected degradation sites. This forms the input to calculations of the probability of tube severs. Single and multiple tube severs are included. From the perspective of evaluating the possibility of large leakage from an LBLOCA event the probability of even a single tube rupture is small. However, considering LBLOCA loads in standard CMOA evaluations would have considerable impact as discussed in Section 8.9.

## 8.1 AXIAL STRENGTH OF DEGRADED TUBES

Figure 8-1 shows a plot of best estimate axial strength using the same best estimate equations and input described in Section 6. Local bending is restricted from degradation within tubesheet crevices leading to the upper strength curve. The degradation severity is expressed as the effective circumferential length which is the circumferential length of 100%TW degradation having the same PDA as a partial depth flaw. Lower leakage areas combined with the higher strength of degradation within the tubesheet crevice leads to the focus on freespan degradation.

For partial depth semi-elliptical flaws with a circumferential length, L and maximum depth expressed as a fraction of the wall thickness, $M_d$, the effective circumferential length, $L_{eff}$ is given by:

$$L_{eff} = \pi/4*(L* M_d)$$

For a bounding load of 3300 lbs. the effective circumferential length of a 100% TW flaw leading to a tube sever is 0.38 inches. Using the above equation leads to the combinations of circumferential length and maximum depth causing a freespan tube sever at 3300 lbs. This is illustrated in Figure 8-2 by the solid line.

For a circumferential flaw of uniform depth, d, the axial load, $F_{leak}$ when the partial depth flaw breaks through the wall thickness to create a leakage path is:

$$F_{leak} = \pi\,(R_i)^2*1.2\,(\sigma_y + \sigma_u)\,M_{cp}\,t\,/\,R_m$$

$$M_{cp} = (1 - d/t)/[1 - d/(t\ M_c)]$$

$$M_c = 0.887 + .1312\,\lambda + 0.1125\,/\,\exp(\lambda)$$

$$\lambda = L\,/\,\sqrt{(R_m\,t)}$$

where: t is the wall thickness
$R_i$ is the inner tube radius
$R_m$ is the mean tube radius

The above equations can be applied to flaws with a semi-elliptical shape in the following manner. Successively larger portions of the semi-elliptical flaw are evaluated starting at mid length. For each portion selected, the average depth under the semi-elliptical flaw is calculated. This average depth and length portion is substituted in the above equations to calculate $F_{leak}$. As the portion length increases the calculated value of $F_{leak}$ will exhibit a minimum value and then increase. The minimum value for $F_{leak}$ is the axial load at which the flaw will tear through the wall thickness to create a leakage path. The maximum depth and total circumferential length of semi-elliptical flaws where $F_{leak}$ is equal to 3300 lbs. is plotted in Figure 8-2 as a dotted line.

After the flaw pops or tears through the wall thickness to create a leakage path, the geometry of the flaw has changed. This will have a small impact on the axial strength. If the length of the pop through region is less than $L_{eff}$ for the axial load of interest a tube sever cannot occur. The dashed line on Figure 8-2 from $L_{eff}$ at 3300 lbs. to the intersection of the leak and tube severs curves represents the estimated effect of flaw tearing through the wall thickness on the axial strength. The region between the dotted line and the dashed line represents the combinations of maximum depth and total circumferential length where leakage will develop at 3300 lbs. without the occurrence of a tube sever. The small size of the leakage without tube sever region results in a small probability of occurrence. In addition the resulting leakage area is a small fraction of the area associated with a tube sever. It is about one order of magnitude smaller. Thus, tube sever is the only issue of concern.

Since LBLOCA axial loads vary with radial position in the bundle and tube-to-shell ΔT, Figure 8-3 illustrates the combinations of maximum depth and circumferential extent of flaws leading to both tube rupture and the development of leakage without a tube sever for an axial load less than the 3300 lb yield load. A value of 2600 lbs. was chosen and is based on the periphery tube load for a 250F tube-to-shell ΔT, Figure 5-2. This load is also similar to the load in the inner most tubes for the 370F tube-to-shell ΔT case.

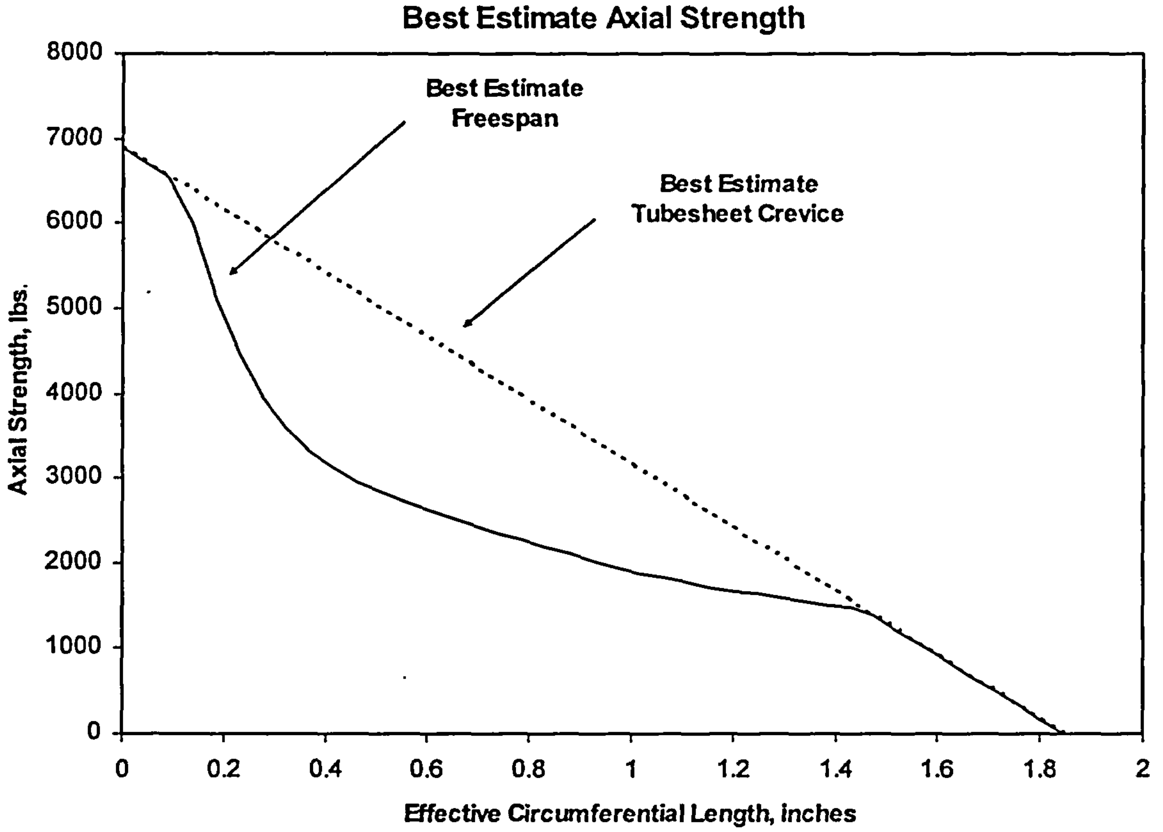



Figure 8-1 Axial Strength versus Effective Circumferential Length

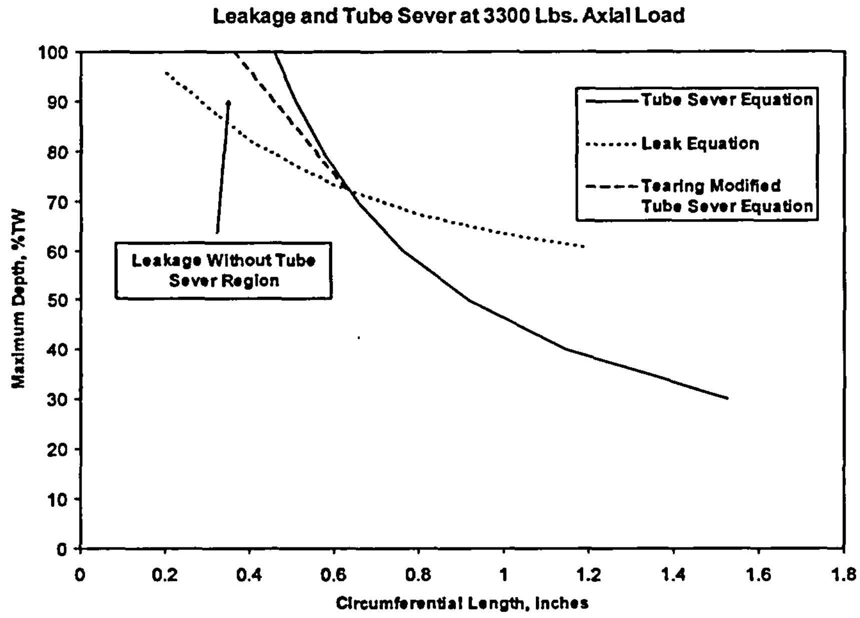



Figure 8-2: Maximum Depth and Circumferential Length Required For Leakage and Tube Sever at 3300 lbs.

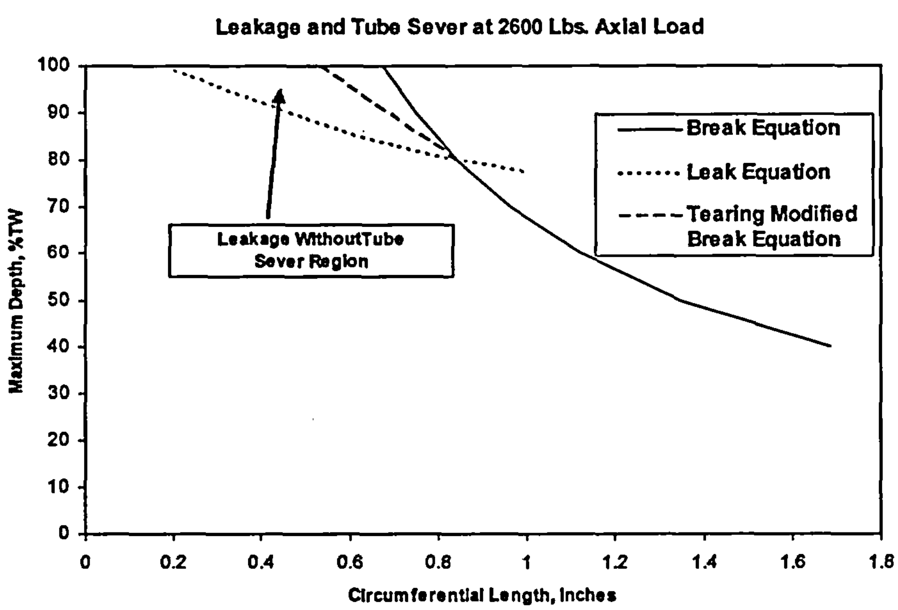



Figure 8-3: Maximum Depth and Circumferential Length Required For Leakage and Tube Sever at 2600 lbs

## 8.2 EXPECTATIONS FROM PAST SERVICE PERFORMANCE

It is instructive to examine the sizes of freespan circumferential cracks and volumetric degradation that has been observed in the past to determine if any tube severs would have occurred at the upper bound LBLOCA load of 3300 lbs. Figures 8-4 through 8-6 plot maximum depth versus circumferential extent for flaws found at Davis Besse, Crystal River Unit 3 and Oconee Unit 1. Freespan degradation at these plants bound that found at other OTSG plants. The results are plotted for the last several outages. The different symbols denote A and B steam generators. No freespan tube severs would have occurred. Leakage without a tube sever is indicated for four indication at Oconee Unit 1. This data was examined in detail. Nondestructive examination (NDE) measured depths are believed to be overestimates due to low signal amplitude. The best estimate result is that neither leakage nor tube severs would have occurred at bounding LBLOCA axial loads. This leads to the expectation that even a single tube freespan tube sever is a low probability event for bounding case LBLOCA loads.

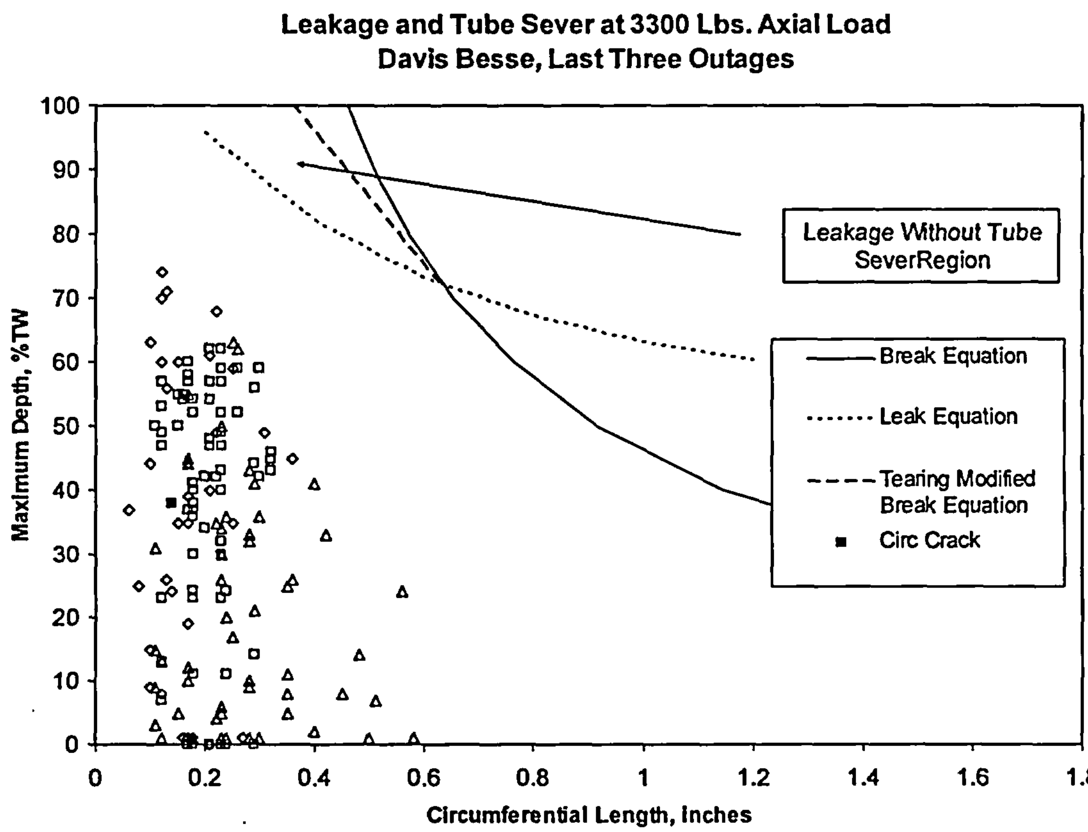



Figure 8-4: Maximum Depth and Circumferential Length for Freespan Volumetric IGA and Circumferential Cracking at Davis Besse

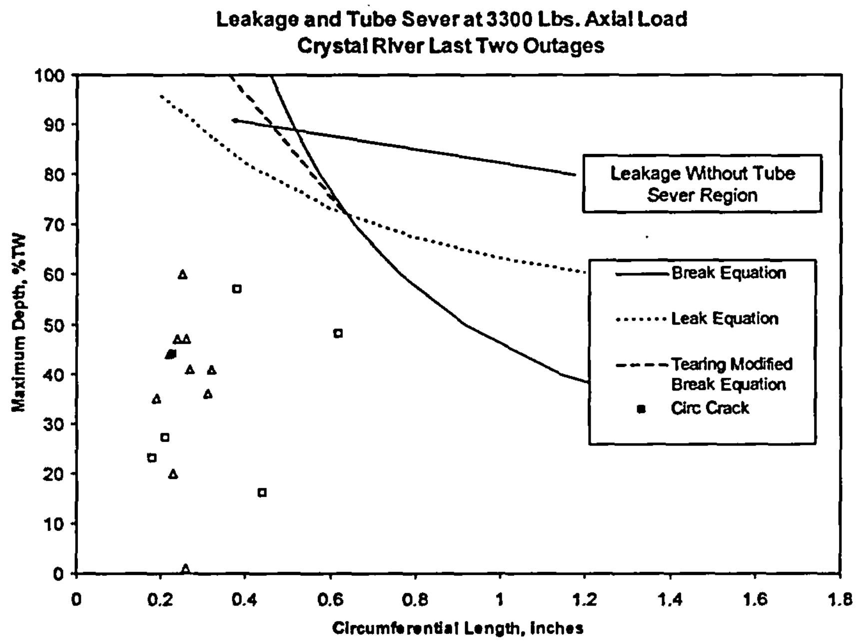



Figure 8-5: Maximum Depth and Circumferential Length for Freespan Volumetric IGA and Circumferential Cracking at Crystal River Unit 3

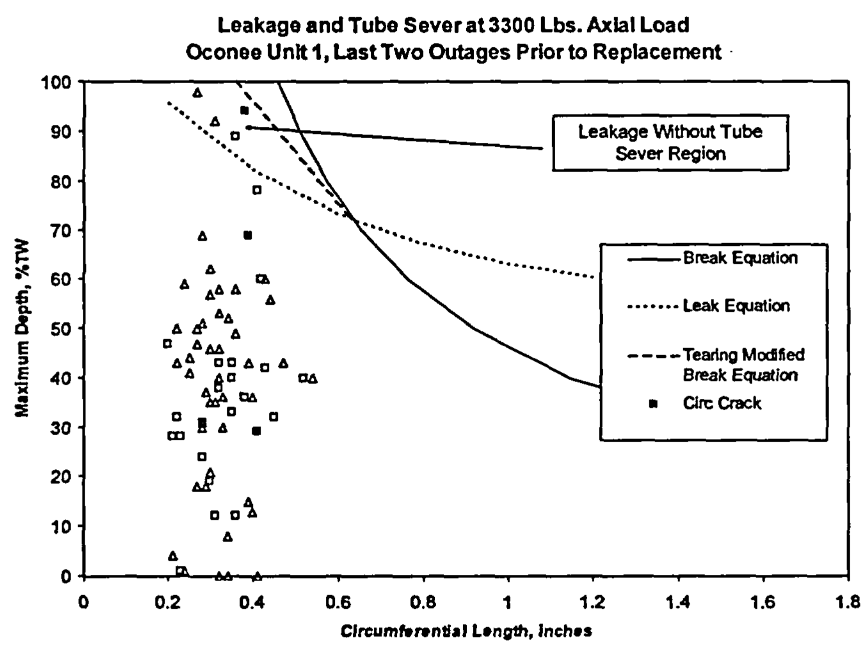



Figure 8-6: Maximum Depth and Circumferential Length for Freespan Volumetric IGA and Circumferential Cracking at Oconee Unit 1

## 8.3 METHODOLOGY OF PROBABILITY CALCULATIONS

The most detailed and sophisticated approach to tube integrity evaluations is a full Monte Carlo simulation of the processes of degradation initiation, growth and NDE inspection over multiple cycles of steam generator operation. In this manner both detected and undetected populations of degradation are tracked and can be evaluated for tube rupture and leakage. Years of experience with this technique, benchmarked against actual service experience leads to the following important and useful conclusions for a plug on detection repair scenario:

- The distribution of end-of-cycle (EOC) degradation lengths after multiple cycles of operation becomes relatively stable.
- The distribution of detected depths of degradation becomes relatively stable and approximates the shape of the probability of detection curve for the applied eddy current inspection technique.
- The probability of a previously (i.e. last outage) undegraded site leading to tube rupture is extremely low and can be neglected.

These observations, supported by sophisticated Monte Carlo calculations and actual plant experience, provide the basis for a relatively simple method of calculating the probability of tube severs under LBLOCA loads.

The future distribution of EOC degradation lengths can be determined from past observations. The future distribution of degradation depths can also be determined from past observations and checked against the eddy current probability of detection (POD) curve. Obviously, trending of the number of past indications versus operating time can be used to develop projections for the number of indications at future times of interest. Since LBLOCA axial loads are a function of radial position within the tubesheet, the radial distribution of degradations sites must be considered.

With the above as input, the probability of a tube sever can be calculated per detected degradation site. For a single degradation site, maximum depth and circumferential extent is selected from the appropriate EOC distributions. Tensile properties (yield and ultimate strength) are assigned from the known distribution that is characteristic of the OTSG fleet. The strength of the degraded tube is then compared to the LBLOCA load of interest. This constitutes one Monte Carlo trial. After many trials, the number of tube severs divided by the number of trails is the best estimate of the probability of a tube sever on a per indication basis. Given this probability and the number of degradation sites of interest, the binomial distribution can be used to determine the probability of 0,1,2...etc tube severs for an LBLOCA event. The details of these calculations are presented in Section 8.8.

## 8.4 DISTRIBUTION OF EOC DEGRADATION LENGTHS

Figure 8-7 shows a plot of degradation lengths at Oconee Unit 1 for the last two outages prior to replacement. The solid line is a ln normal fit to the bounding distribution. It is a good representation of the worst case distribution that can be expected in a steam generator which has operated for about 22 EFPY. The only operating original OTSG's not in their last cycle before replacement are Davis Besse, Crystal River Unit 3 and TMI-1. Of these, TMI-1 is the lead plant at 19 EFPY. A ln normal length distribution with a mean (ln values) of -1.1436 and a standard deviation of 0.2436 is a good bounding distribution of degradation lengths for these plants at their projected end of life after 2 or 3 more operating cycles.

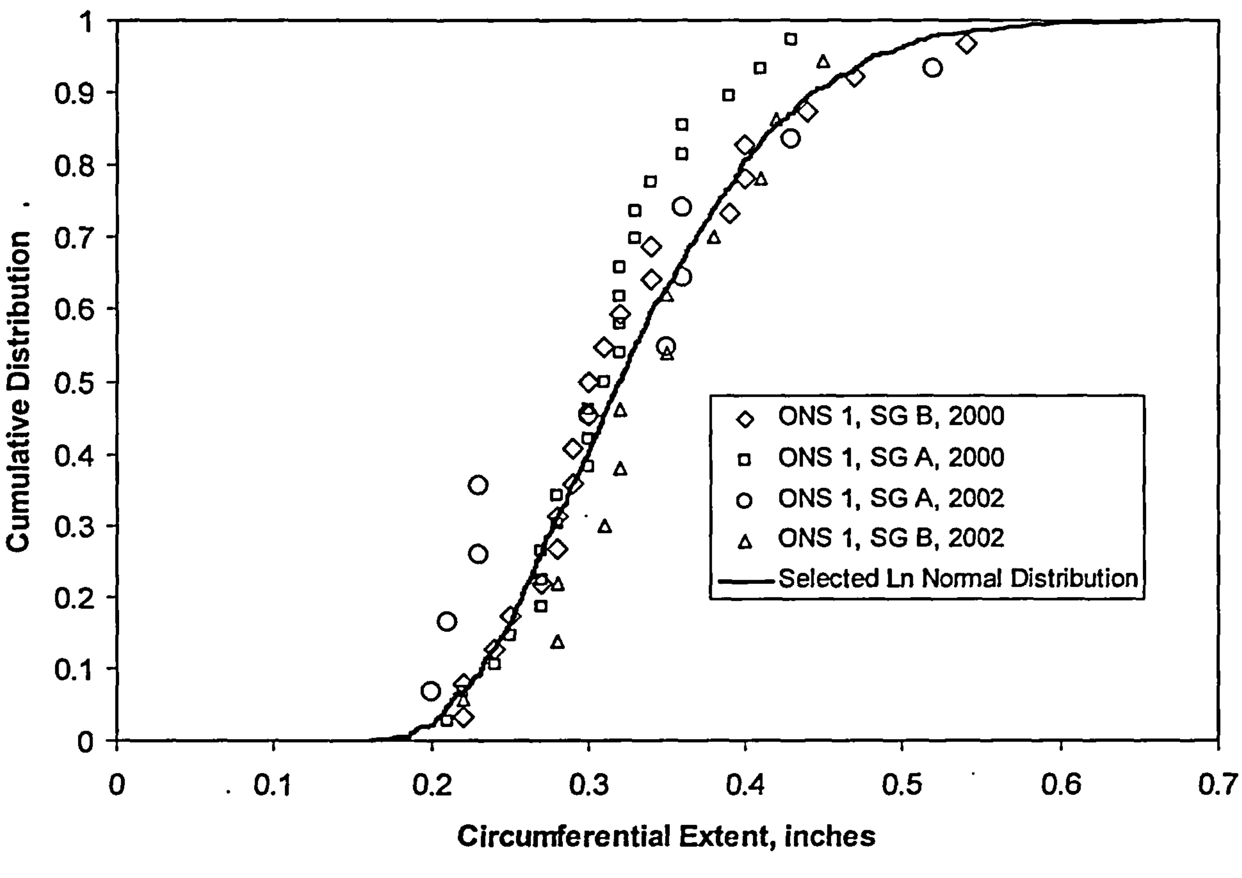



Figure 8-7: Distribution of Circumferential Lengths for Freespan Degradation at Oconee Unit 1

## 8.5 DISTRIBUTION OF EOC DEGRADATION DEPTHS

Distributions of NDE measured maximum depths of volumetric degradation at Crystal River Unit 3, Davis Besse and Oconee Unit 1 for the last one or two inspections are plotted in Figure 8-8. NDE sizing uncertainty distorts the tails of these distributions, skewing the lower tail to smaller depths and the upper tail to larger depths. A review of the data for the 4 largest depths shows that depths have been overestimated. In 3 cases this was due to low eddy current signal amplitude. In the remaining case the presence of a dent distorted the phase angle depth measurement. A log logistic curve was fitted to the trend band of the depth distribution using the mid point of the band and the average cumulative distribution at about 60% TW. This fitted depth distribution is given by:

$$CDF_{Depth} = 1/(1 + \exp(25.84-15.89*\log_{10}(M_d)))$$

Since depth distributions become stable after multiple cycles of operation with a plug on detection repair scenario and the same inspection technique and calling criteria, the fitted distribution can be used to represent the depth distribution expected in the future. Naturally as more degradation sites develop in the future and more selections from this depth distribution are made, the number of instances of degradation sites with large depths will increase.

As noted earlier the detected depth distribution becomes about equal to the POD curve after multiple cycles of operation with a plug on detection repair scenario and the same inspection technique and calling criteria. Figure 8-9 shows 2 POD curve of interest. The dotted line is a POD curve for volumetric IGA in the upper tubesheet crevice at ANO based on pulled tube data. It is slightly more adverse than the detected distribution curve at large degradation depths. This is the region that dominates calculations of the probability of tube sever. Upper tubesheet degradation at ANO is believed to be due to sulfur incursions on the secondary side in the early years of steam generator operation. Tube pulls show that this type of degradation can lead to instances of deep degradation with short circumferential extents. This morphology would lower POD values at large depths. The POD curve is log logistic given by:

$$CDF_{Depth} = 1/(1 + \exp(17.23-10.99*\log_{10}(M_d)))$$

The dashed line is more representative of the general freespan degradation at issue for large LBLOCA leakage. This POD curve is based on an extensive study of 85 pulled tube degradation sites and includes multiple analysts to include analyst uncertainty. It has a logistic form and is given by:

$$CDF_{Depth} = 1/(1 + \exp(4.429-0.1169*M_d))$$

The detected distribution curve is selected as the best estimate distribution. The two POD curve are considered as reasonable bounds to the upper tails of the distribution of EOC maximum depths. Circumferential cracking is detected with the Plus Point eddy current probe. The POD curve applicable to this type of degradation is bounded by the curve of Figure 8-9. Hence the same distribution of maximum depths can be applied to both volumetric IGA and circumferential cracking.

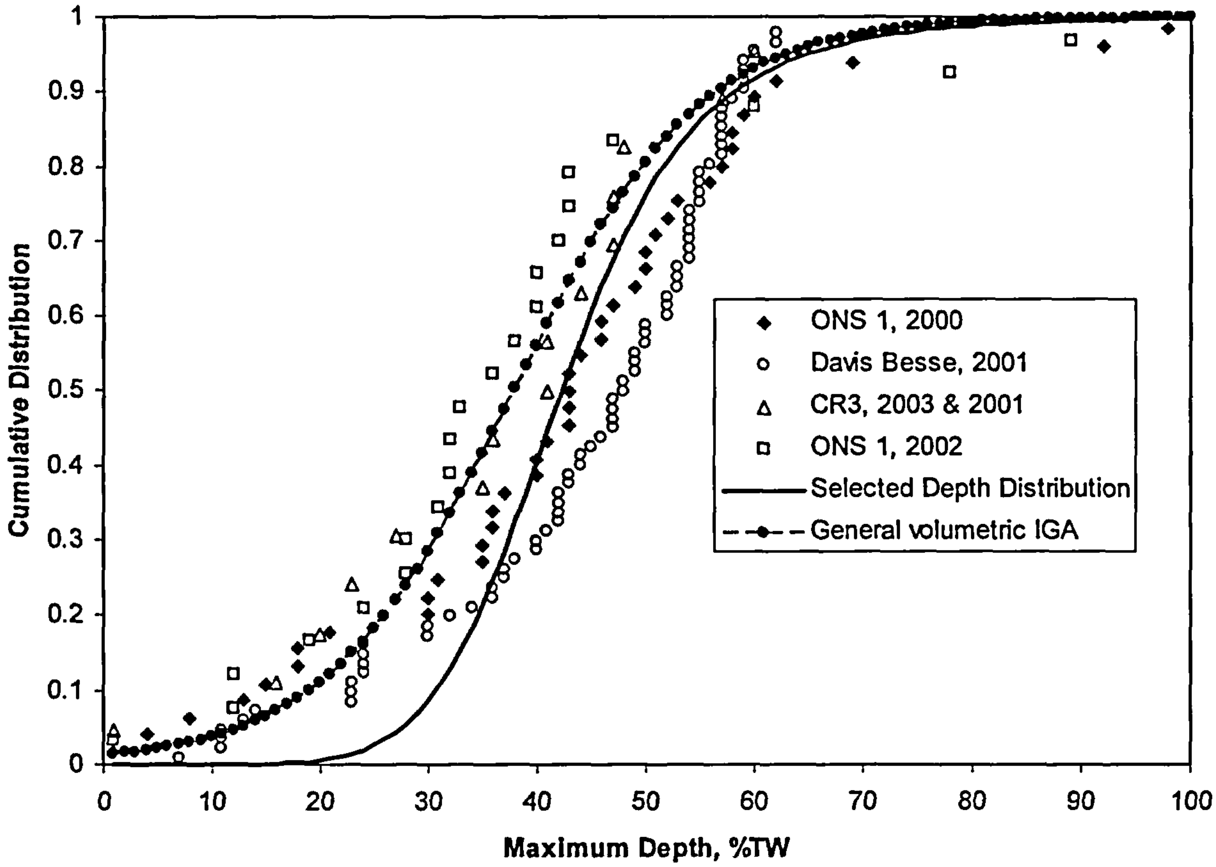



Figure 8-8: Distribution of Maximum Depths for Freespan Volumetric Degradation at OTSG Plants

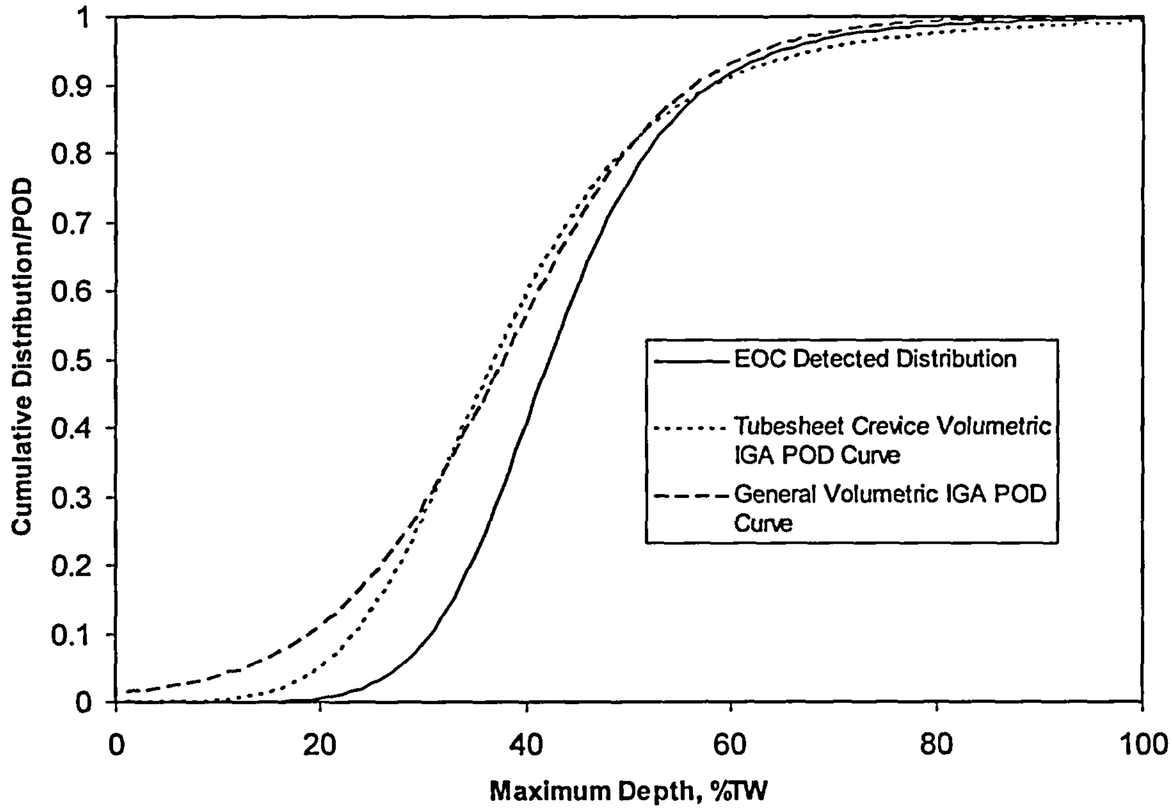



Figure 8-9: Distributions of End of Life Maximum Depths, Best, Lower and Upper Estimates

## 8.6 PAST AND PROJECTED NUMBER OF DEGRADATION SITES

The numbers of indications of freespan volumetric IGA and circumferential cracking for all steam generators in the OTSG fleet are plotted in Figure 8-10 as a function of effective full power years of operation. The dotted lines indicate steam generators which have been replaced or which will be replaced after the current cycle of operation. The trend lines are highly erratic with little evidence for a systematic continuously increasing progression of degradation. The largest number of indications detected was 67 at Davis Besse. This increase over past performance is believed to be due to an inspection transient resulting from chemical cleaning at the previous outage. A significant decrease is expected at the next outage. From past experience, a total of 40 to 60 indications in the worst case steam generator is a good worst case end of life EOC projection for the three plants of interest; Davis Besse, Crystal River Unit 3 and TMI-1. Steam generator replacement at these plants is expected after about 3 more cycles of operation.

It should be noted the types of freespan degradation that are not included in the trend curves of Figure 8-10 includes the freespan volumetric degradation in the first span at Crystal River Unit 3 to which an alternate repair criteria has been applied, and the circumferential cracking seen at the lower tubesheet secondary face of SG A at RFO 11.

The first span volumetric degradation has maximum depths which rarely exceed 40 %TW and the distribution of circumferential lengths is much lower than the plots of Figure 8-7. Circumferential cracking at the lower tubesheet secondary face in SG A at RFO 11 was viewed as an artifact at the time of the inspection but conservatively called as circumferential cracking with consequent tube plugging. No indications of this type appeared at the next inspection supporting the original best estimate of signal artifacts.

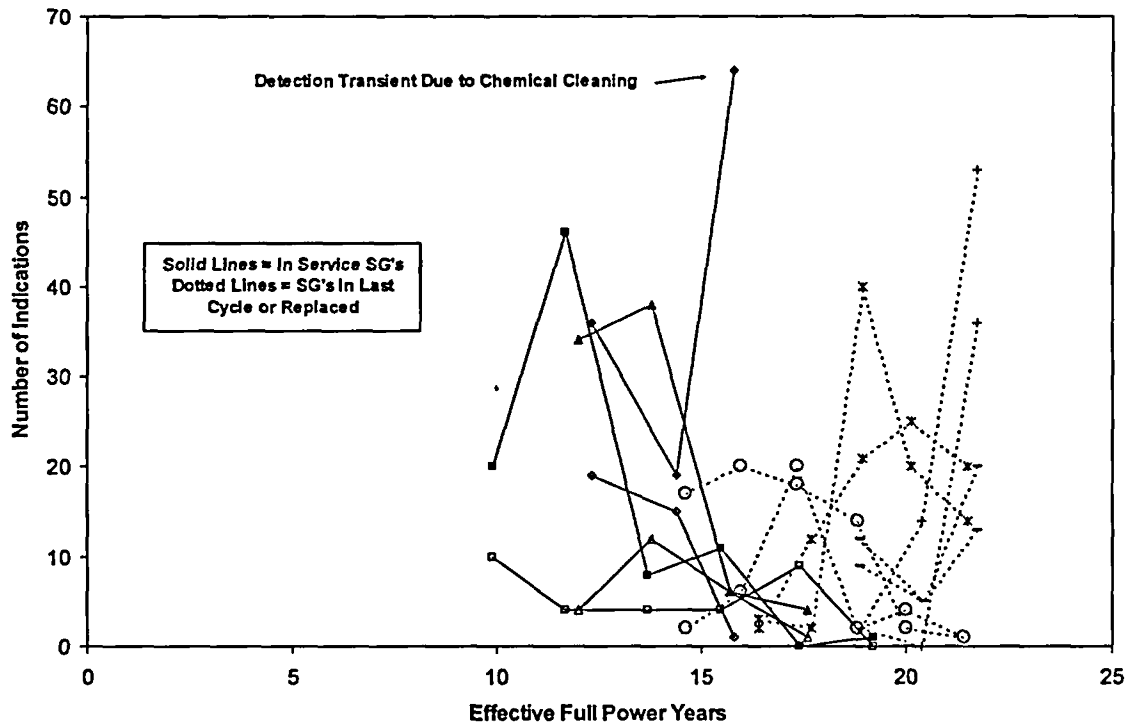



Figure 8-10: Number of Freespan Indications with Circumferential Extent versus Effective Full Power Years, All OTSG Plants

## 8.7 PAST AND PROJECTED RADIAL LOCATIONS OF DEGRADATION SITES

Since LBLOCA axial loads are highest at the periphery of the bundle, degraded tubes in the periphery dominate calculations of the probability of tube sever. To account for the variation in tube loads, radial distributions of freespan volumetric IGA and circumferential cracking were evaluated. The results show there is no trend of radial distribution of degradation from one steam generator to the next. Figures 8-11 through 8-14 demonstrate this point. The number of indications in bins of radial distance is plotted versus radial distance. In some cases degradation is located primarily in the center of the bundle, Figure 8-11, while in others most degradation is in the periphery, Figure 8-12. Others steam generators approach a more or less uniform radial distribution.

Of the 3 original steam generators of interest, Crystal River Unit 3 and TMI-1 had too few indications of freespan degradation in the past 2 inspections to indicate a trend of radial distribution. Davis Besse had a large number of indications. From Figure 8-11 it is seen that most of this degradation was near the center of the bundle. A reasonable conservative choice for an end of life projection is to place half of the total number of projected indications at the periphery. The worst case projection is thus 20 to 30 degradation sites at the highest load location.

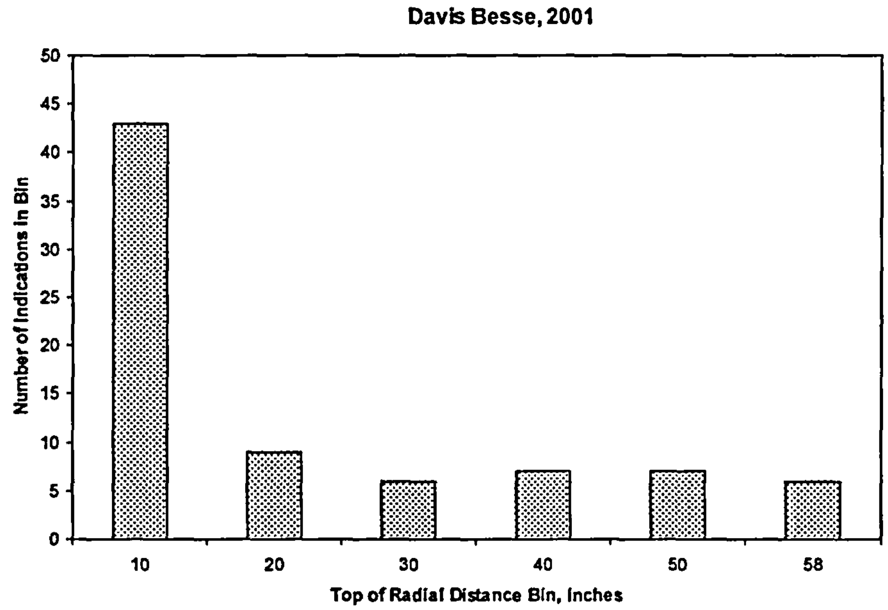



Figure 8-11: Radial Distribution of Freespan Indications with Circumferential Extent, Davis Besse, 2001

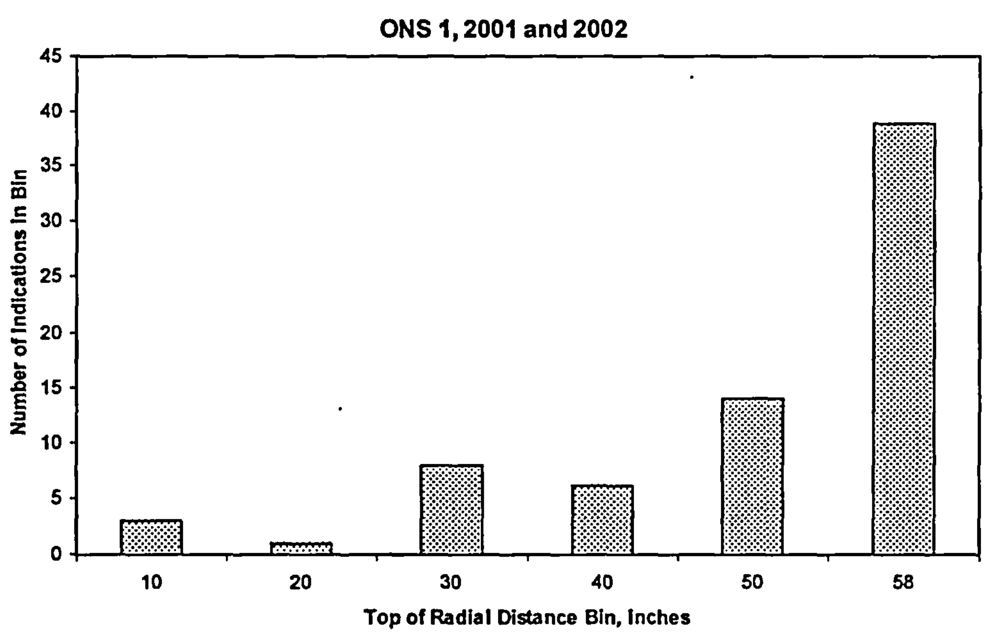



Figure 8-12: Radial Distribution of Freespan Indications with Circumferential Extent, Oconee Unit 1, 2001 and 2002

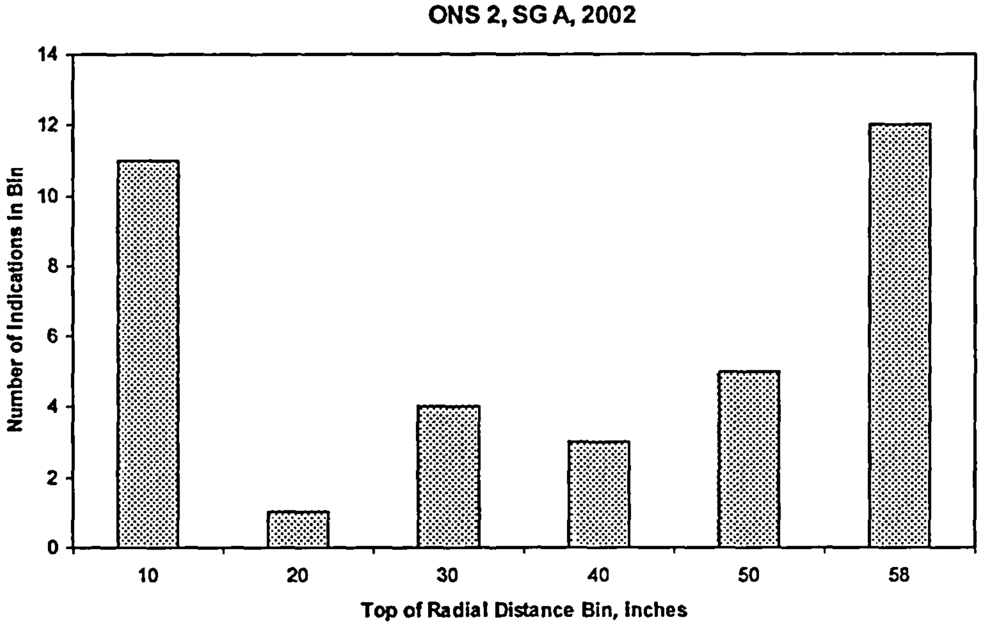



Figure 8-13: Radial Distribution of Freespan Indications with Circumferential Extent, Oconee Unit 2, SG A, 2002

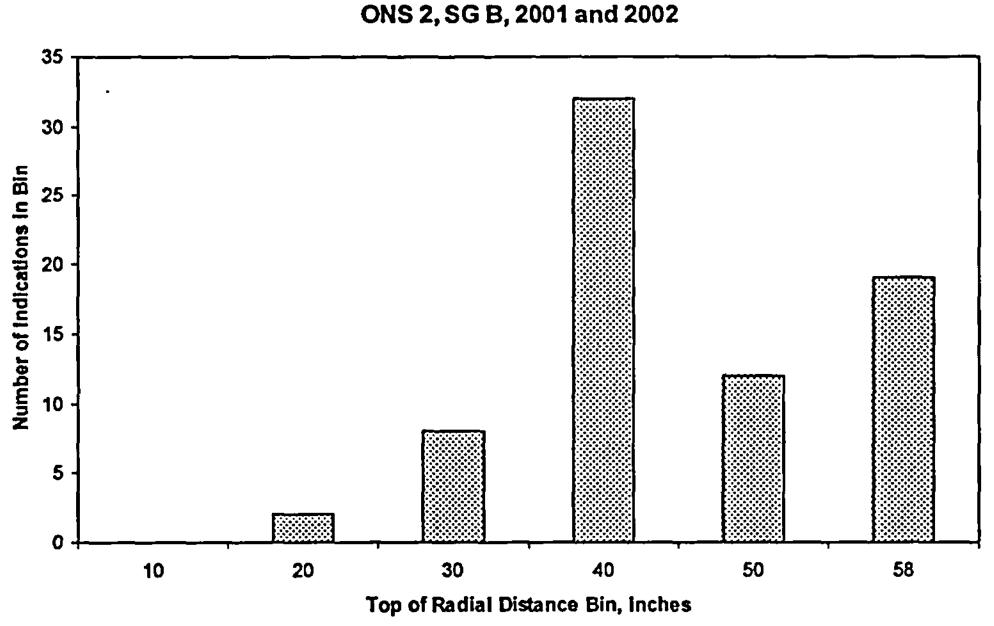



Figure 8-14: Radial Distribution of Freespan Indications with Circumferential Extent, Oconee Unit 2, SG A, 2002

## 8.8 CALCULATIONS OF FREESPAN TUBE SEVER PROBABILITY

A Mathcad program was written to implement the calculation methodology described in Section 8.3. This program is listed in Figure 8-15. It computes the number of tube severs expected in $10^6$ Monte Carlo trials as a function of the desired LBLOCA axial load. This number divided by $10^6$ is the best estimate probability of a tube sever on a per indication basis. The distribution of Yield + Ultimate Strength is a normal distribution with a mean of ~148,000 psi with a standard deviation of ~8,000 psi. The uncertainty in the axial strength equation of Section 6 is included. Calculations were performed using the 3 EOC maximum depth distributions of Section 8.5. Only one EOC degradation length distribution was used, the ln normal distribution of Section 8.4.

Figure 8-16 shows the calculated probability of a tube sever per indication (degradation site) versus axial load. The probability of a tuber sever per indication rapidly decreases as the applied load decreases. Thus considering all projected peripheral degradation sites to be located at the highest axial load is conservative. The best estimate EOC end of life depth distribution leads to a probability of tube sever per indication of 7.2e-4. This value is increased by a factor of about 3 if the depth distribution is approximated by the POD curve for upper tubesheet crevice volumetric IGA at ANO. It is decreased by a factor of 2 if the more generally applicable freespan volumetric IGA POD curve is used as the EOC depth distribution.

Since more than one degradation site is expected in the worst case generator at end of life, the binomial distribution must be used to determine the probability of 0,1,2,...X tube severs for an LBLOCA event at this worst point of operating history. If the probability of tuber sever per indication is p, the probability of exactly X tube severs considering N indications, $P_x(N)$ is given by:

$$P_x(N) = \frac{N!}{X! \bullet (N-X)!} \bullet p^X \bullet (1-p)^{(N-X)}$$

This is a classic marbles in a box problem. If you reach into a very large box containing white marbles and black marbles N times, what is the probability of ending up with exactly X black marbles if the probability of selecting a black marble on a single try is p?

Figure 8-17 shows a plot of probability of occurrence versus number of tube severs for various number of degradation sites at end of life using a bounding axial load of 3300 lbs. Assuming 30 degradation sites in the periphery at end of life, the best estimate of the probability of no tubes severs is 0.978565. The probability of 1 tube sever is 0.021211. The probability of more than 1 tube sever is then 1-0.978565-0.02121 = 0.000224. Clearly LBLOCA is a single tube sever event.

A single tube sever event is still the case for the upper estimate of 0.002039 for the probability of tube sever per indication at 3300 lbs. With this per indication probability, the probability of no tube severs is 0.940605, 1 tube sever is 0.057654 and more than 1 tube sever is 1-0.940605-0.057845 = 0.00174. Figure 8-18 is a repeat of Figure 8-17 using the upper estimate of the probability of tube sever per indication. The single tube sever nature of an LBLOCA event is evident even for relatively large numbers of degradation sites at end of life. As a worst case upper bound at very low probability 2 or 3 tube severs might considered in evaluating leakage consequences.

$$STP(Load) :=$$

$$Z1 \leftarrow runif(1000000, 0, 1)$$

$$Z2 \leftarrow rnorm(1000000, -1.1436, 0.2436)$$

$$Z3 \leftarrow rnorm(3000000, 0, 1)$$

$$\text{for } i \in 0..999999$$

$$LMD_i \leftarrow \frac{25.84}{15.89} - \left(\frac{1}{15.89}\right) \cdot \ln\left[\frac{\left(1 - Z1_i\right)}{Z1_i}\right]$$

$$MD_i \leftarrow \begin{cases} 10^{LMD_i} & \text{if } 10^{LMD_i} < 128 \\ 128 & \text{otherwise} \end{cases}$$

$$LEN_i \leftarrow \exp\left(Z2_i\right)$$

$$Leff_i \leftarrow 0.01 \cdot \frac{\pi}{4} \cdot MD_i \cdot LEN_i$$

$$m \leftarrow i + 100000$$

$$n \leftarrow i + 200000$$

$$Strength_i \leftarrow 148000 + 8000 Z3_i$$

$$M1_m \leftarrow 0.01659 Z3_m$$

$$M2_n \leftarrow 0.011504 Z3_n$$

$$Eta_i \leftarrow \frac{\left(Leff_i\right)}{2 \cdot \pi \cdot 0.294}$$

$$Eta_i \leftarrow 0 \text{ if } Eta_i < 0$$

$$Eta_i \leftarrow 0.999 \text{ if } Eta_i \geq 1$$

$$FAX1_i \leftarrow Strength_i \cdot \pi \cdot 0.294 \, 0.037 \left[1.2128 \left(\frac{0.29}{0.275}\right)^2 \cdot \left(1 - Eta_i\right) + M1_m\right]$$

$FAX2_i \leftarrow$ Equation blanked out --Framatome ANP proprietary

$$FAX_i \leftarrow FAX1_i \text{ if } FAX1_i < FAX2_i$$

$$FAX_i \leftarrow FAX2_i \text{ if } FAX2_i \leq FAX1_i$$

$$Break_i \leftarrow \begin{cases} 1 & \text{if } FAX_i < Load \\ 0 & \text{otherwise} \end{cases}$$

$$Break_i$$

$$TB \leftarrow \sum Break$$

$$TB$$

Figure 8-15: Monte Carlo Program for Calculation of Probability of Tube Sever per Indication

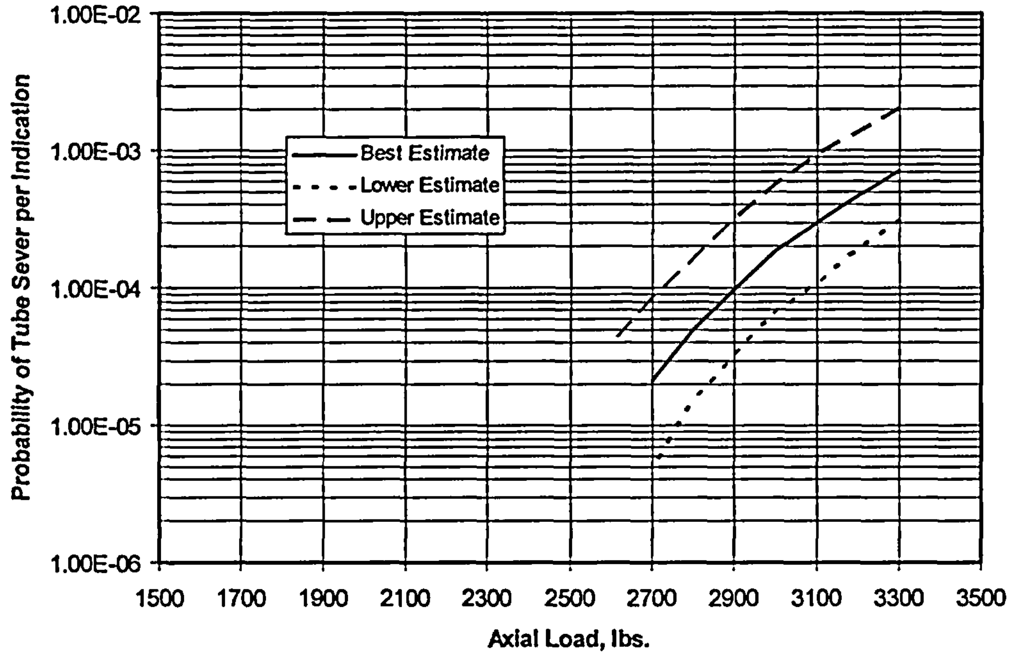



Figure 8-16: Probability of Tube Sever Per Indication versus Axial Load

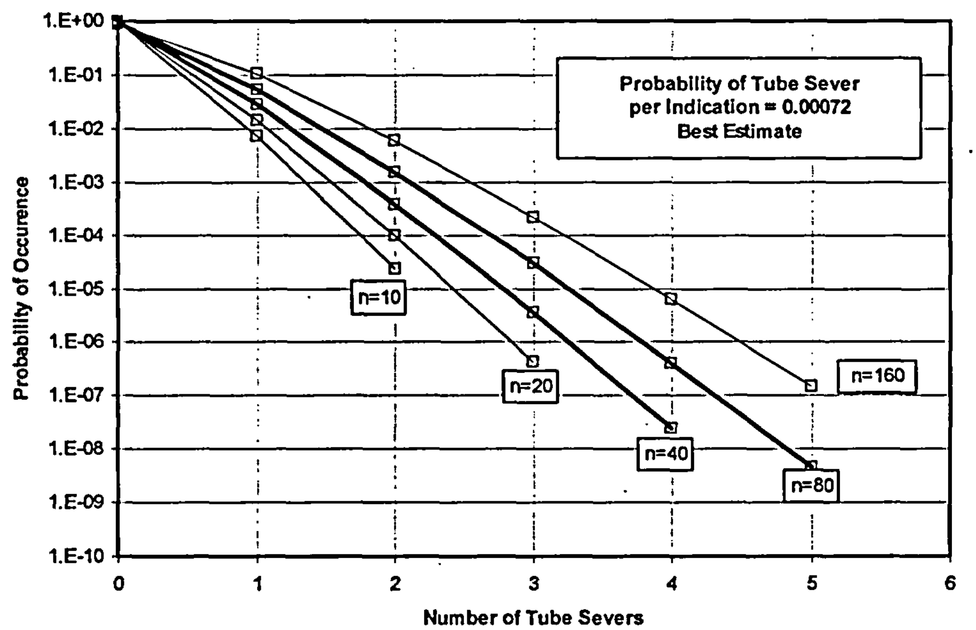



Figure 8-17: Probability of Occurrence versus Number of Tube Severs, Best Estimate

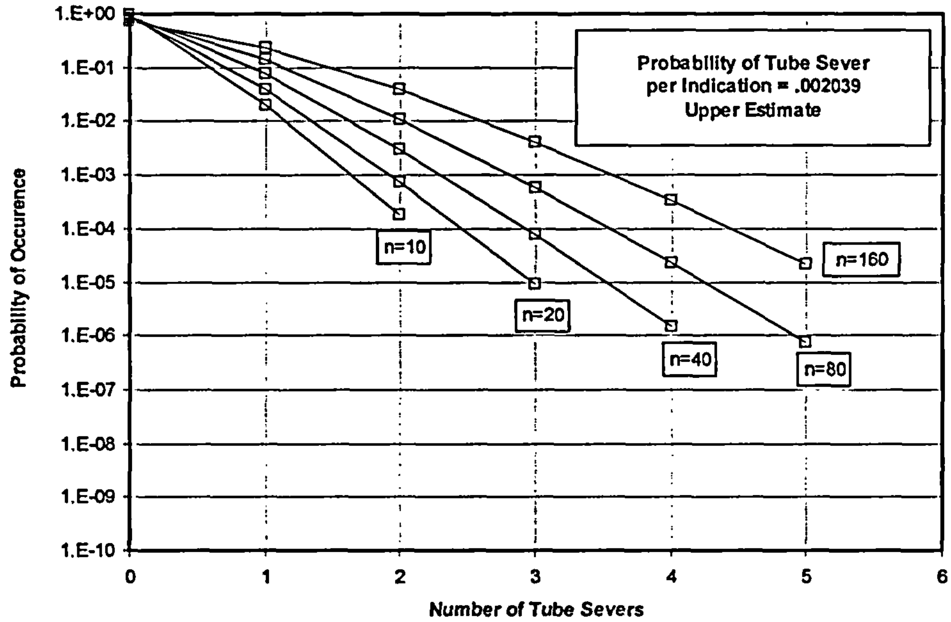



Figure 8-18: Probability of Occurrence versus Number of Tube Severs, Upper Estimate

## 8.9 IMPLICATIONS OF LBLOCA LOADS ON CMOA EVALUATION

The focus of LBLOCA leakage integrity is the issue of the probability of multiple tube severs. A probability of a single tube sever on the order of several percent is good news. This is not the case for standard CMOA tube integrity evaluations. The figure of merit for a tube rupture at accident conditions is 1% at 95% confidence and the leak rate limit is 1 gpm. Of the three OTSG plants not replaced or in the final cycle of operation, only Davis Besse has observed more than a few freespan degradation sites with significant circumferential extent. Due to specialized thermal hydraulic conditions axial loads in the periphery at Davis Besse during a steam line break (SLB) are equal to the maximum axial loads for an LBLOCA event. Thus maximum axial loads in the periphery are already considered in CMOA evaluations. There is no need to consider including LBLOCA loads as design basis loads in tube integrity evaluations.

## 9.0 SUMMARY AND CONCLUSION

LBLOCA loads have been determined as a function of radial position and maximum temperature differential. Flaw tolerance under these loads has been evaluated using best estimate practices with discussions of the effect of uncertainties provided in Section 8.9. In terms of the probability of developing large leakage areas, freespan tube severs is the issue of concern. Tubing degradation with some circumferential extent, volumetric IGA and circumferential cracking is the limiting consideration. A quantitative review of past inspection results for OTSG plants shows that a freespan tube sever is a low probability event even at bounding axial loads. Projected end of life maximum degradation depths and lengths and numbers of degradation sites considering radial position allowed calculation of the probability of a tube severs. The probability of a single freespan tube sever at conservatively projected end of life conditions is 0.02. The probability of more than 1 tube sever is 0.0002. Sensitivity studies confirm that LBLOCA is clearly a single freespan tube sever event. As a worst case upper bound at very low probability, 2 or 3 tube severs might be considered in evaluating leakage consequences.

## 10.0 GENERAL REFERENCES

1) AREVA/FANP Document BAW-2374 "Risk-Informed Assessment of Once-Through Steam Generator Tube Thermal Loads due to Breaks in RCS Upper Hot Leg Piping", March 2001

2) "Steam Generator Degradation Specific Management Flaw Handbook", Final Report, January 2001, Report 1001191, EPRI, Palo Alto, Ca.

## ATTACHMENT 3

## BAW-2374 (Justification to Exclude LBLOCA as a Design Basis Accident for SG Tube Loads) Methodology – Source Term for Resulting Dose

### Background

In February of 2003, BWOG representatives met with the NRC to discuss the progress on the review of BAW-2374, Revision 1. During the meeting, the NRC informed the BWOG that this revision was not acceptable based on the fact that the long term cooling requirements of 10CFR50.46 were not adequately shown to be met. Through follow-up correspondence, it became apparent that the requirements of 10CFR100 had not been adequately addressed either. Therefore, a task to study the effects of the LBLOCA on the fuel cladding was initiated and B&W RELAP5 calculations were performed to predict the likelihood of fuel clad rupture. It was proposed that if an evaluation were performed which bounded all BWOG plants that predicted no cladding rupture, 10CFR100 requirements would be met, since the downstream effect of steam generator tube leakage would be no worse than dose evaluations already performed as part of the BWOG plants' licensing bases.

### BWOG Plant Current Licensing Bases for Related Accident Dose Consequences

In addition to the spectrum of LOCA break sizes and locations which satisfy the requirements of 10CFR50.46, the BWOG plants' licensing bases include dose consequences of LOCAs of various sizes and at various locations. The LOCAs analyzed for dose consequences range from a DEG (Double Ended Guillotine) large break to a CRE (Control Rod Ejection) small break. The DEG break could occur anywhere in the RCS piping, and the CRE occurs in the reactor vessel head. The source term assumed for the CRE is commensurate with the amount of fuel expected to fail during the event. The DEG source term in at least one of the licensee's documentation assumes a source term of the gap material of the core. In addition to the LOCA dose analyses, the SGTR (Steam Generator Tube Rupture) analysis conservatively assumes a 1% failed fuel source term where 1% of the fuel rod gas inventory is released.

The BWOG plants licensing bases also include a LOCA dose using the MHA (Maximum Hypothetical Accident) source term. This source term is a theoretical release of 1% of the solid fission products, all of the noble gases, and half of the iodine in the core. This assumes that the gap gases from every fuel rod has been released and a portion of the core has melted as described in TID-14844 or other hypothetical source term methodology. The stated purpose of the MHA is to calculate a dose larger than any which could occur for establishing the exclusion area boundary of the plant to conservatively protect the public and bound any accident.

### Regulatory Requirements

10CFR100.11 addresses what source term should be assumed in establishing the site radiological boundaries. Footnote 1 of that regulation is reproduced below:

"[1] The fission product release assumed for these calculations should be based upon a major accident, hypothesized for purposes of site analysis or postulated from considerations of possible accidental events, which would result in potential hazards not exceeded by those from any accident considered credible. Such accidents have generally been assumed to result in substantial meltdown of the core with subsequent release of appreciable quantities of fission products."

For plants which have incorporated AST (Alternate Source Term) methodology into the plant licensing basis, paragraph 2.1 of Regulatory Guide 2.1 offers this guidance:

"The AST must be based on major accidents, hypothesized for the purposes of design analyses or consideration of possible accidental events, which could result in hazards not exceeded by those from other accidents considered credible. The AST must address events that involve a substantial meltdown of the core with the subsequent release of appreciable quantities of fission products"

There are three fission product containment barriers which must be breached for dose consequences of any accident to affect the public. The first barrier is the fuel cladding. The second is the RCS. And the third barrier is the secondary piping or the containment building. If any one of these barriers is maintained, no added dose consequences due to an accident will be realized, and an accident is not considered credible for significant dose consequences. And if the fuel cladding remains intact for any event, a source term equivalent to that assumed for the MHA as discussed in the regulatory excerpts above is not credible.

**Clad Rupture Study for a LBLOCA Located in the Top of the Candy Cane**

Using the conservative EM (Evaluation Model) methodology, the expected fuel failures were calculated for the BWOG plants for a LBLOCA Located in the Top of the Candy Cane. This work was performed for the B&WOG as a generic study that can be used for all the B&W-designed plants. Bounding assumptions were made so that this analysis is generically applicable to every BWOG plant for the hot leg LOCA cladding rupture study.

**Assumptions**

In general conservative assumptions which bound all plants were used for this evaluation. The double-ended guillotine cold leg pump discharge (CLPD) LOCA with a discharge coefficient of 1.0 is the limiting break for all of these plants as documented in each plant's licensing basis for the purpose of calculating 10CFR50.46 limits. The LHR limit that is obtained from the CLPD LBLOCA LHR limit analyses effectively normalizes the differences in the EM methods, CFT initial conditions, and CFT line resistance variations, along with the fuel design differences (such as fuel pellet diameter, cladding thickness, and CHF variations between the Mark-B10, Mark-B11, Mark-B12, Mark-B-HTP, etc.). Because the CLPD LHR limits are used to limit design peaking in the core for the plants, a break in the hot leg should yield less severe results than a break which occurs in the cold leg region.

Initial Power Level:

For LL (Lower Loop) plants, a power level of 2568 MWt was used in this analysis along with the 10 CFR 50 Appendix-K requirements for power level uncertainty (1.02). For the RL (Raised Loop) plant, a power level of 2966 MWt is assumed. Both the RL and LL analyses assumed a decay heat multiplier of 1.2 along with the B&W heavy isotopes for decay heat to maximize the decay heat post trip. A conservative TIL (Time in Life) was assumed with limiting burnup and associated pin pressure.

Fuel Type:

For the LL plants, the limiting BHTP CHF correlation maximized heat-up. The RL plant utilized the BWC CHG correlation and the Mark-B10K fuel with M5 cladding.

CFT:

The limiting combination of the initial cover gas pressure (minimum) and liquid volume (maximum) within the CFT was used to result in a worse-case response. A nominal fluid temperature was used to balance the competing results of maximum tube loads and core cooling.

ECCS Temperature:

The temperature of the ECCS liquid plays a minor role in determining the cladding temperature response. Higher temperatures result in less core cooling and a higher probability of cladding rupture. However, the SG tube-to-shell temperature differential is maximized by considering the coldest ECCS temperatures. If a maximum ECCS temperature were considered, the results of the tube load analysis would be significantly better. Because the overall issue is the integrity of the SG tubes, artificially penalizing another aspect of the same analysis is not warranted. Therefore, a nominal range ECCS temperature of 70 F was assumed for this analysis. DB assumed 62.5F based on a nominal range of temperatures.

Single Failure Assumption:

A LOOP (Loss of Offsite Power) was not assumed in the SG tube load analyses and no single failure in the ECCS was assumed. The result rapidly cooled the core. For the cladding rupture study, a LOOP was assumed at the time of turbine trip, which is coincident with the break opening. Postulating a LOOP would affect the RCP operation in the analyses. RCP operation for a hot leg break would improve the core flow response and delay DNB.

A single failure for ECCS was not assumed. If cladding rupture were to occur, it would be early in the event before pumped injection had an affect on the results. Also, less ECCS flow would have the positive effect of hotter SG tubes, decreasing the tube-to-shell $\Delta T$. There is no other active single failure that could affect the clad rupture study.

**Results**

LL- Taking the difference in the cladding temperature and the rupture temperature for each EM pin at the peak unruptured node, the limiting hot pin at MOL with a pin pressure of 2750 psia has the lowest margin to rupture of approximately 99 F.

RL – The liming hot pin at MOL with a pin pressure of 2750 psia has the lowest margin to rupture of approximately 72 F.

**Conclusion**

The conclusion of the cladding rupture study is that the cladding integrity is not compromised during the hot leg break event which produces the most limiting SG tube-to-shell delta-T for either type of B&W plant (i.e., LL and RL) using conservative methodology and using nominal to conservative input assumptions. . Therefore, without the breach of the fuel cladding, no large source term exists, and the radiation release is due to the radioactive products contained in the coolant and not the fissionable material or gap activity within the fuel itself. With respect to 10CFR100 implied requirements, the assumption of the use of an MHA source term for this event is not credible, and therefore a realistically bounding source term no worse than that which could exist for normal operation should be used for dose consideration. It is reasonable to assume that the existing licensing bases of the BWOG plants for dose consequences, then, giving proper consideration for the expected tube leak rate, would bound the dose consequences of the LBLOCA at the top of the candy-cane.