Vogtle Electric Generating Plant Units 1 and 2
License Amendment Request to Revise Technical Specification (TS)
Sections 5.5.9, "Steam Generator (SG) Program" and TS 5.6.10,
"Steam Generator Tube Inspection Report" for Interim Alternate Repair Criterion

Enclosure 8

Westinghouse Electric Company LLC, LTR-CDME-08-043 NP-Attachment, "Response to NRC Request for Additional Information Relating to LTR-CDME-08-11 NP-Attachment," dated March 18, 2008

LTR-CDME-08-43 NP-Attachment

Response to NRC Request for Additional Information Relating to LTR-CDME-08-11 NP-Attachment

March 18, 2008

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QUESTIONS RELATING TO STEAM GENERATOR TUBESHEET AMENDMENT ON INTERIM ALTERNATE REPAIR CRITERIA

The NRC has provided to Wolf Creek Nuclear Operating Corporation (WCNOC) by email dated February 28, 2008 the Request for Additional Information (RAI) relating to an interim alternate repair criterion (IARC) that requires full-length inspection of the steam generator tubes within the tubesheet, but does not require plugging tubes if the extent of any circumferential cracking observed in that region greater than 17 inches from the top of the tubesheet that meets the performance criteria of NEI 97-06, Rev. 2, "Steam Generator Program Guidelines," (Reference 1).

A total of thirteen RAI were provided to WCNOC. Four additional RAI have since been provided to Southern Nuclear Operating Company for Vogtle Units 1 and 2. The same four additional RAI were also provided to Exelon Generation Company for the Braidwood Nuclear Power Station. The responses to RAI 6 through 17 are provided below.

After adjusting for growth as documented in Reference 2, the allowable crack sizes in the tube (203°) and the weld metal (94°) are bounding values and they apply for Model D5, Model F, Model 44F and Model 51F steam generators. The 1.0 inch axial separation criterion discussed herein for multiple circumferential cracks also applies to these same model steam generators. The ASME Code stress report results summarized in response to RAI 9 apply to the Model F steam generator only; however, it has been confirmed that similar results have been obtained for the Model D5 steam generators.

6. *Figure 3-7 (LTR-CDME-08-11-P) needs to provide all geometry details assumed in the weld analysis on pages 7, 9 and 10. (The NRC staff does not understand the assumed weld geometry based on the discussion on pages 7, 9 and 10.) With respect to the equation for S.A. near the top of page 10, what is the parameter whose value is 0.020 and what is the solution for "y"?*

Response: The tube-to-tubesheet weld is modeled in Figure 6-1 below. The tube wall has an inner radius r_i and an outer radius r_o, and it is displaced upward [

]^{a,c,e}

a,c,e

Figure 6-1

The equation of a line, relative to the ellipse is:

y = mx + b, where

the slope = $\tan\theta$, and one point is located at (r_o, 0.020). The resulting equation for the line on which the crack grows is:

[ ]a,c,e

Similarly, the equation of the ellipse, as offset from the origin, is:

[ ]a,c,e

where [ ]a,c,e

Simultaneously solving the equations for the line and the ellipse results in the point of their intersection (x, y):

[ ]a,c,e

Setting the points so that they are now relative to the original coordinate system gives the point (x', y').

[ ]a,c,e

The surface area of the frustum, S.A., is calculated by the surfaces of revolution technique and is

[ ]a,c,e

where, the equation for the line can be rewritten as:

[ ]a,c,e

and

Thus,

$$\frac{dx}{dy} = \cot\theta$$

a,c,e

[ ]

and the result is:

a,c,e

[ ]

The previous calculation made use of surfaces of revolution (Φ varies from 0 to 2*π) in order to calculate the surface area of the entire frustum. Now, since the circumferential flaw does not subtend a surface completely around the frustum, the equation must be integrated over an angle of revolution (Φ to Φ+ΔΦ). In addition, as the crack grows along the line of crack propagation, the y-value is integrated from y' to y'+d*sinθ, where d is the crack depth. Thus, in this case, the surface area of the flaw, A_f, is:

a,c,e

[ ]

the final result of which is:

a,c,e

[ ]

The surface area of the circumferential flaw, A_{fc}, is a hybrid of the previous two. The angle of revolution again varies from 0 to 2 π, as in the case of the surface area of the frustum. However, the y-value varies from y' to y'+d*sinθ, just as in the case of the partially circumferential flaw.

Now the integral is:

a,c,e

[ ]

and the result is:

a,c,e

[ ]

7. *On page 10, the assumed flaw is said to extend a distance "d" into this "surface." Does "surface" refer to the outer ellipse or inner ellipse in Figure 3-5? Figure 3-5 suggests it is from the inner ellipse.*

Response: Referring to the frustum pictured in Figure 3-4 on Page 16 of LTR-CDME-08-11, viewing the frustum from above (looking down) or viewing the frustum from below (looking up), the view obtained is shown in Figure 3-5. The crack originates in the bottom of the frustum in Figure 3-4 and grows upward along the surface depicted. That is what the crack in Figure 3-5 is attempting to show. The crack originates at the point (x', y') in the first figure provided to answer Question 6.

8. *What was the assumed flow stress for the weld material? What was the basis for selecting this value?*

Response: The weld is an autogenous weld; no filler metal is used. The flow stress assumed for the weld bead is the same as that of the tube (base) metal, which was taken from Westinghouse WCAP-12522 (Reference 3). This is a conservative assumption since the Alloy 182 weld metal used for the tubesheet clad is stronger than the base metal of the tubing. Manufacturer's specifications[1] for Alloy 182 and Alloy 82 weld metal indicate that the yield strength ranges from [ ]^{a,c,e} and the ultimate tensile strength ranges from [ ]^{a,c,e} The flow stress ($0.5*(S_Y+S_{UT})$) then ranges from [ ]^{a,c,e} This range of values is higher than the flow stress used in the tube ligament analysis [ ]^{a,c,e}

9. *LTR-CDME-05-209-P (Reference 5) states that the tube-to-tubesheet welds were designed and analyzed as primary pressure boundary in accordance with the requirements of Section III of the ASME Code. Provide a summary of the Code analysis, including the calculated maximum stress and applicable Code stress limit.*

Response:

General Summary of ASME Code Stress Report Results Relative to the IARC

The existing Model F steam generator tube end weld (TEW) analysis used an axisymmetric finite element model (FEM) to estimate the stress state of the weld material. The assumptions in the weld analysis (Reference 2) closely resemble the assumptions in the IARC (LTR-CDME-08-11-P). For example, in the Model F FEM analysis there is [ ]^{a,c,e}

This result is similar to the [ ]^{a,c,e} plane cited in LTR-CDME-08-11-P when the different weld surfaces are compared (i.e., the flat plane chosen in the Model F FEM geometry versus the elliptical plane used in LTR-CDME-08-11-P). Therefore, the results described for the limiting weld ligament in LTR-CDME-08-11-P are reasonable. In addition, the stress results contained in WNET-153, Vol. 6 (Reference 6) for a

[1] FAX from Samuel D. Kaiser, P.E., of Inco Alloys Int'l, Inc. Welding Products Co. dated August 31, 1999 to Karan K. Gupta of Westinghouse NEE-Pensacola.

Model D5 steam generator are bounded by those contained in the Model F steam generator report (Reference 4).

Weld Geometry Model

Figure 9-1 shows the configuration of the weld as modeled in the Code stress analysis. This is a conservative idealization of the actual weld bead, which is approximately an [ ]a,c,e The interfacing elements to the weld have been added to Figure 9-1 for clarity.

a,c,e

[ ]

Figure 9-1

The average actual height of the weld bead was determined by destructive examination of 10 factory welds and was found to be [ ]a,c,e The modeled height of the weld was conservatively set at [ ]a,c,e To maximize the load applied to the weld, since the dominant loading is tubesheet deflection, a "stiff" tube of [ ]a,c,e wall thickness was assumed.

Stress Summary

The results of the stress analysis are contained in Table 9-1 for the limiting section of weld [ ]a,c,e

Table 9-1

a,c,e

Quantity	Design	Emergency	Faulted	Test

Note: P_m is the primary membrane stress intensity

The design primary membrane stress intensity is based on the design pressure differential of [ ]^{a,c,e} and an isothermal temperature of [ ]^{a,c,e} from the Equipment Specification.

Loads and Loading Conditions

There are four sources of applied loads on the weld material:

- Deformation imposed by the tubesheet motion (taken at the center of the tubesheet, assuming no restraint from the divider plate, to maximize the tubesheet deflection). This is the most significant of the loads.
- Primary-to-secondary pressure differences.
- Local temperature gradients. Shown to be "trivial" in the Code stress analysis.
- Isothermal temperature. Local temperature gradients are very small. (Exception: Non-ductile failure evaluation.)

Weld residual stress is not considered because it is stated to be insignificant compared to the operating loads. This is because the ASME Code stress report analysis assumes that there is [

]^{a,c,e}

The end cap loads and fatigue results for the tube end weld were evaluated for several ASME Code defined conditions as specified in the Equipment Specification for the Model F steam generator. The conditions in the analysis included:

- Design Condition
- Normal and Upset Conditions
- Emergency Conditions
- Faulted Conditions
- Test Conditions

Material Properties

The materials used in the FEA model are:

- Tubesheet Ligament: SA-508 Cl 2a
- Tube: SB-163 (Code Case 1484)
- Tubesheet Cladding: Inconel Weld

See the tables below for a detailed description of the appropriate data from the applicable Code year.

TABLE 4-1

MATERIAL PROPERTIES VS. TEMPERATURE FOR SA-508-CL. 2a

Temperature	TC (Btu/hr-ft-°F)	TD (ft^2/hr)	$\alpha \times 10^6$ (in/in-°F)	$E \times 10^{-6}$ (psi)	S_m (ksi)	S_y (ksi)	S_u (ksi)

a,c,e

TC = Thermal Conductivity

TD = Thermal Diffusivity

α = Mean Coefficient of Expansion going from 70°F to indicated temperature.

E = Modulus of Elasticity

S_m = Design Stress Intensity

S_y = Yield Strength

S_u = Ultimate Strength

TABLE 4-2

MATERIAL PROPERTIES VS. TEMPERATURE FOR SB-163 (Code Case 1484)

Temperature	TC (Btu/hr-ft-°F)	TD (ft^2/hr)	$\alpha \times 10^6$ (in/in-°F)	$E \times 10^{-6}$ (psi)	S_m (ksi)	S_y (ksi)	S_u (ksi)

a,c,e

TC = Thermal Conductivity

TD = Thermal Diffusivity

α = Mean Coefficient of Expansion going from 70°F to indicated temperature.

E = Modulus of Elasticity

S_m = Design Stress Intensity

S_y - Yield Strength

S_u = Ultimate Strength

The thermal properties and the elastic modulus of the cladding are assumed to be the same as those for the tube.

Thermal Analysis

The thermal analysis considered a bounding transient for Normal and Upset conditions, Inadvertent RCS Depressurization. For this transient, the maximum calculated temperature difference between the nodes represented in the FEA model is [ ]^{a,c,e} It was concluded that the [

]^{a,c,e}

Method of Analysis

The analysis was performed with an axisymmetric finite element analysis in the WECAN computer program with a very fine nodal mesh in the weld area and its interfaces with the tube and the tubesheet clad. The elements consisted of [

]^{a,c,e} Applied loads were due to deformation imposed by the tubesheet motion, primary-to-secondary pressure differences, local temperature gradients, and isothermal temperature.

Calculated Stresses

The following tables are reproductions of the tables included in the code stress analysis for the tube end weld.

Table 7-5 shows that the [

]^{a,c,e} The section numbers in Table 7-5 correspond to the section numbers in the model description figure above. In order to demonstrate acceptability, [

]^{a,c,e}

TABLE 7-1

DESIGN CONDITION STRESSES

Section	Stress Category	Stress Components, ksi σ_Z σ_R τ_{RZ} σ_H	Principal Stresses, ksi S_1 S_2 S_3	Maximum Stress Intensity, ksi	Allowable Stress Limit, (ksi)

a,c,e

TABLE 7-2

EMERGENCY CONDITION STRESSES

Section	Stress Category	Stress Components, ksi σ_Z σ_R τ_{RZ} σ_H	Principal Stresses, ksi S_1 S_2 S_3	Maximum Stress Intensity, ksi	Allowable Stress Limit, (ksi)

a,c,e

TABLE 7-3

FAULTED CONDITION STRESSES

Section	Stress Category	Stress Components, ksi σ_Z	σ_R	τ_{RZ}	σ_H	Principal Stresses, ksi S_1	S_2	S_3	Maximum Stress Intensity, ksi	Allowable Stress Limit, (ksi)

a,c,e

TABLE 7-4

TEST CONDITION STRESSES

Section	Stress Category	Stress Components, ksi σ_Z	σ_R	τ_{RZ}	σ_H	Principal Stresses, ksi S_1	S_2	S_3	Maximum Stress Intensity, ksi	Allowable Stress Limit, (ksi)

a,c,e

TABLE 7-5

NORMAL AND UPSET CONDITION
PRIMARY PLUS SECONDARY STRESS INTENSITY RANGE

Location	$(P_L + P_b + Q)_{max}$** (ksi)	Allowable Limit $3S_m$ (ksi)

a,c,e

* Section numbers are identified in the figure included with the Weld Geometry Description, above.

** All transients creating primary-plus-secondary stress intensity ranges greater than $3S_m$ are evaluated inelastically.

Summary of Fatigue Usage from Code Stress Analysis of the Tube End Weld:

a,c,e

[ ]

The point of maximum usage factor, where [ ]^{a,c,e} is the most likely fatigue crack initiation point, although the usage is still less than 1.0.

Non-Ductile Failure Evaluation

The methods of evaluating non-ductile failure are [ ]^{a,c,e}

10. Regarding the weld repair criterion:

a. A detailed stress analysis (e.g., finite element) would be expected to reveal a much more complex stress state than that assumed in the licensee's analysis, which may impact the likely locations for crack initiation and direction of crack propagation. In addition, the dominant stresses for crack initiation and crack growth may involve residual stresses in addition to operational stresses. Also, flaws may have been introduced during weld fabrication. Thus, the 35-degree conical "plane" is not the only plane within which cracks may initiate and grow.

b. One hypothetical crack plane, which appears more limiting than the one assumed by the licensee, is the cylindrical "plane" defined by the expanded tube outer diameter where the weld is in a state of shear. Assuming a flow stress of 63.7 ksi and an effective weld depth of 0.035 inches (as shown in LTR-CDME-05-209-P, Figure 2-1), the NRC staff estimates that the required circumferential ligament to resist an end cap load of 1657 lb is greater than 180 degrees (without allowances).

Address these concerns and provide a detailed justification for why the submitted analysis is conservative.

Response: Weld residual stress (WRS) was not considered since there is no definitive basis for any value used. Both the original Wolf Creek code stress analysis and a more recent code stress analysis for different models of steam generators dismiss residual stresses in the weld as negligible.

Development of credible residual stresses using FEA methods is extremely difficult, particularly for small welds like the tube-end weld. A comprehensive test program involving deep/shallow hole drilling, or finite element analyses which include the birthing of elements under very high temperatures to simulate the welding process would be required in order to develop a value for use. Verification of finite element WRS analysis results by deep/shallow hole drilling can only be accomplished for larger volumes of weld metal as removal of cores of trepanned material is required. For small volumes of weld metal, verification of the finite element analysis is much more difficult and thus, the WRS values assumed are more uncertain.

In the ASME Code stress analyses, the operating loads on the weld are characterized as overshadowing any effects of WRS. Current development of residual stress models (unpublished) for consideration as a Code Case indicate that the stress on the inner diameter of the tube is compressive, and not conducive to crack opening. The WRS values used as the basis of the modeling were taken from the heat affected zone (HAZ) of stainless steel welds; therefore, the actual WRS profile may be different. The profile is tensile in some areas and compressive in others (only tensile components of WRS have a deleterious effect). Consideration of WRS further complicates the analysis, but does not necessarily add any conservatism.

The weld region is not in a state of pure shear. There are tensile loads as well as the pressure acting on the face of the weld exposed to primary coolant. Therefore, the limits for pure shear (ASME B&PV Code Section III, NB-3227.2) are not considered to apply. Thus, the ASME code is satisfied with respect to pure shear. The shear plane used in the IARC weld ligament calculation was only used to calculate the shear component of the stress state. This is consistent with the original Wolf Creek code stress analysis in which shear was not explicitly considered, and the shear plane identified was not found to be the limiting plane. The most likely crack initiation point, due to fatigue usage, was on a plane extending from the weld root almost normal to the face of the weld. A recent code stress analysis for another plant did consider pure shear explicitly and determined that the weld region is not in a state of pure shear, thus supporting the WCNOC stress analysis. This report definitively stated that the pure shear limit of NB-3227.2 ($0.6S_m$) does not apply.

The crack opening performed in the weld region for the Wolf Creek IARC was assumed to open due to maximum principal stress, which is tensile, and flow stress was chosen as the limiting strength parameter. While reviewing the Wolf Creek IARC report, it was found that the component stresses, which generate the principal stresses, were not being recalculated as the flaw grew. The correction to this problem (see below), which is documented in Reference 7, changed the bounding required remaining ligament for partially circumferential flaws in the weld region to [ ]^{a,c,e} (not adjusting for growth) from the approximately [ ]^{a,c,e} originally reported in LTR-CDME-08-11 P-Attachment (reference Table 3-3). The value of [ ]^{a,c,e} supersedes the old value of [ ]^{a,c,e} Westinghouse believes that these corrections make the consideration of the flaw area in the left hand side of the force balance equations correct.

The normal stress component was:

a,c,e

[ ]

The normal stress component now is:

a,c,e

[ ]

The shear stress reported in the Wolf Creek IARC was:

a,c,e

[ ]

The shear stress component, until the flaw breaches the weld root is now:

a,c,e

[ ]

b is the semi-minor axis (0.014 inch). This is due to the shear path being uninterrupted until that point. After breaching the weld root, there is a lack of a stress path. The shear stress at that point, is:

a,c,e

[ ]

11. ***The proposed tube and weld repair criteria do not address interaction effects of multiple circumferential flaws which may be in close proximity (e.g., axial separation of one or two tube diameters). Address this concern and identify any revisions which may be needed to the alternate tube repair criteria and the maximum acceptable weld flaw size.***

Response: In order to ascertain how far apart cracks must be in order to be considered to respond independently to an applied far field stress, a fracture mechanics approach was undertaken. The assumed case was [

]a,c,e

[

][a,c,e] Therefore, a conservative estimate of the distance necessary to prevent the interaction between cracks is [

][a,c,e] and is equal to 1.0 inch. It is also worthy to note that 1.0 inch, which is between 1 and 2 tube diameters, bounds the 0.5 inch result contained in the ASME Boiler and Pressure Vessel Code, Section XI, Article IWA-3000.

a,c,e

Figure 11-1. Individual Steam Generator Results for the Distance Necessary for σ_{yy} to Equal σ

a,c,e

Figure 11-2. Combined Steam Generator Results for the Distance Necessary for σ_{yy} to Equal σ

The impact of the crack separation analysis is summarized below. Refer to Figures 11-3 through 11-5 for explanations of the crack geometries and combinations of crack-like indications considered in the analysis. Table 11-1 is a summary of the text description of the crack separation analysis impacts. The details described in Table 11-1 apply only to the portion of the tube within the tubesheet 17 inches below the top of the tubesheet (TTS-17 inches).

An Industry Peer Review was conducted on March 12, 2008 at the Westinghouse Waltz Mill Site with the purpose of reviewing the Fall 2007 Catawba Unit 2 cold leg tube end indications to establish whether the reported indications are in the tube material or the weld material. A consensus was reached that the 2007 Catawba Unit 2 cold leg indications most likely exist within the tube material. However, some of the indications extend close enough to the tube end that the possibility that the flaws do extend into the weld could not be ruled out. Therefore, in order to address the potential for cracking in the tube weld in parallel to crack-like indications in the tube, the more limiting ligament size of [$]^{a,c,e}$ (including the adjustment for growth) for the weld is used to establish the allowable crack size in the tube for cracks less than 1.0 from the tube end.

Crack-like indications in a tube:

1. If any circumferential crack-like indication in the tube exceeds 203°, plug the tube.

2. If there is more than one circumferential crack-like indication in a tube, and no single crack angle exceeds 203°, and the minimum axial distance of separation between the crack-like indications is

greater than or equal to 1.00 inch, then the maximum crack angle is used to describe the flaw and the tube remains in service.

3. If there is more than one circumferential crack-like indication in a tube, and no single crack angle exceeds 203°, and the minimum axial distance of separation between the crack-like indications is less than 1.00 inch, and the non-overlapping sum of the crack angles plus the overlapped crack angle is less than or equal to 203°, the tube may remain in service.

4. If there is more than one circumferential crack-like indication in a tube, and no single crack angle exceeds 203°, and the minimum axial distance of separation between the crack-like indications is less than 1.00 inch, and the non-overlapping sum of the crack angles plus the overlapped crack angle is greater than 203°, plug the tube.

Crack-like indications in a tube less than 1.0 inch from the tube end:

5. If there are one or more cracks in the tube that are each less than or equal to 94°, and there is a minimum axial separation distance between the tube end and the tube cracks of less than 1.00 inch, and the non-overlapping sum of the tube crack angles plus the overlapped crack angle is less than or equal to 94°, the tube may remain in service.

6. If there is a crack-like indication in the weld less than or equal to 94° and there are one or more cracks in the tube that are each less than or equal to 94°, and there is a minimum axial separation distance between the tube end and the tube cracks of less than 1.00 inch, and the non-overlapping sum of the tube crack angles plus the overlapped crack angle is greater than 94°, plug the tube.

Table 11-1: Summary of Crack Separation Analysis and Interactions					
	Multiple Cracks?	Max. Crack Angle in Tube, $\theta^{1,2}$	Max. Crack Angle in Weld, α^{1}	Min. Axial Separation Distance, L	Required Action
Case	-	Degrees (°)	Degrees (°)	inch	-
1	No	> 203	No Crack	N/A	Plug Tube
2	Yes	$\theta_1, \theta_2, \theta_n \leq 203$	No Crack	≥ 1.00	Cracks do not interact. Report max. crack angle less than 203°. Leave in Service.
3	Yes	$\theta_1+\theta_2+\theta_n \leq 203$	No Crack	< 1.00	Sum of total non-overlapping crack angle plus overlap angle less than 203°. Leave in Service.
4	Yes	$\theta_1+\theta_2+\theta_n > 203$	No Crack	< 1.00	Sum of total non-overlapping crack angle plus overlap angle greater than 203°. Plug Tube.
5	Yes	$\theta_1+\theta_2+\theta_n+\alpha \leq 94$	*Possible Crack in Weld*	< 1.00[3]	Sum of total non-overlapping crack angle plus overlap angle less than 94°. Cracks in weld and tube do interact. Leave in Service.
6	Yes	$\theta_1+\theta_2+\theta_n+\alpha > 94$	*Possible Crack in Weld*	< 1.00[3]	Sum of total non-overlapping crack angle plus overlap angle greater than 94°. Cracks in weld and tube do interact. Plug Tube.

1. See Figures 11-3, 11-4 and 11-5 for tube crack angle and weld crack angle definition.
2. θ_n is the sum of any remaining crack angles after the first two crack-like indications. For example, the statement: $\theta_1+\theta_2+\theta_n \leq 203°$ is equivalent to writing: $\theta_1 + \theta_2 + \theta_3 + \ldots \leq 203°$.
3. Separation distance, L, is measured from the tube end.

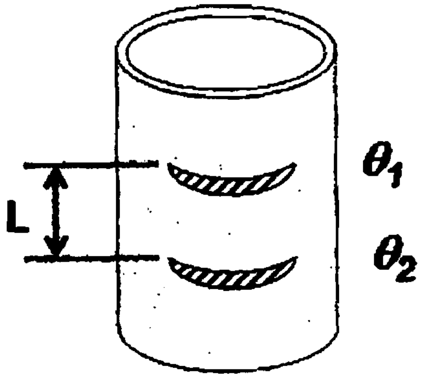

Figure 11-3: Tube Crack Geometry

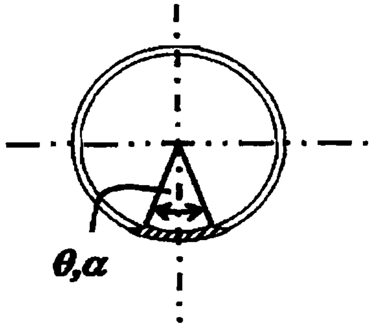

Figure 11-4: Tube and Weld Crack Angle Measurement

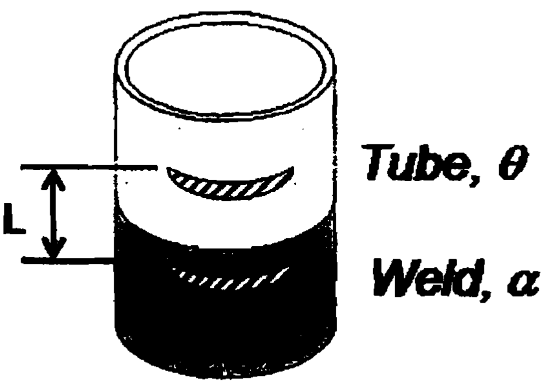

Figure 11-5: Axial Separation Distance Between Weld and Tube Crack-Like Indications

12. The technical support document for the interim ARC amendment does not make it clear how licensees will ensure they satisfy the accident induced leakage performance criteria. Describe the methodology to be used to ensure the accident induced leakage performance criteria is met. Include in this response (a) how leakage from sources other than the lower 4-inches of the tube will be addressed (in the context of ensuring the performance criteria is met), and (b) how leakage from flaws (if any) in the lower 4-inches of the tube will be determined (e.g., determining the leakage from each flaw; multiplying the normal operating leak rate by a specific factor).

Response:

The Modified B* leakage analysis in the IARC report calculates the ratio of undegraded crevice length determined by eddy current inspection to the length of undegraded crevice required to meet the design basis accident analysis primary-to-secondary leakage analysis assumption for the limiting design basis accident. By definition of the IARC, 17 inches from the top of the tubesheet is the available undegraded crevice length because confirmed cracking in this length will require the tube to be plugged. Both the pressure difference ratio and the length of crevice during normal operating and design basis accident are factored in the margin determination.

Referring to Table 4-5 of the IARC report, the limiting design basis accident for WCGS is a postulated steam line break (SLB) event. Referring to Table 4-2 of the IARC report, it is calculated that [ ]^{a,c,e} of undegraded crevice length is required to preclude exceeding the SLB accident analysis leak rate assumption of 0.25 gpm. This corresponds to a safety factor of approximately [ ]^{a,c,e} in terms of the ratio of non-degraded crevice as confirmed by eddy current inspection (17 inches) to the crevice length calculated using the D'Arcy equation necessary to preclude exceeding the SLB accident analysis leakage assumption [ ]^{a,c,e} Therefore, the maximum leakage rate that would occur during a postulated SLB event from cracks occurring 17 inches below the top of the tubesheet is calculated to be [ ]^{a,c,e} from the faulted SG. This provides a margin of [ ]^{a,c,e} on leakage rate for other sources of accident-induced leakage.

The table below shows the available margin for leakage sources other than the tubesheet based on the IARC method for calculating the estimated leakage for which a bounding zero-contact-pressure value of loss coefficient, based on the available test data, is used.

Table 12-1: Calculation of Available Margin for Leakage Sources Other Than in the Tubesheet During the Limiting Plant Design Basis Accident (DBA)

a,c,e

Plant	NOP Leak Limit	Limiting Plant DBA	DBA Leak Limit	L Required for DBA	Safety Margin	DBA Leak Margin Available

The response to Question 13 (following) further clarifies the methodology for satisfying the accident-induced leakage performance criteria. For the underlying assumptions of the IARC – no contact pressure between the tube and the tubesheet in the hydraulic expansion region – the discussion above shows that significant margins exist over the length of the crevice required in the 17 inch span below the top of the tubesheet. However, a conservative factor of 2.5 will be applied to that part of the observed normal operating leakage that cannot be associated with degradation mechanisms outside the tubesheet expansion region to calculate the accident-induced leakage from the tubesheet region. The resulting calculated accident-induced leakage will be added to the predicted leakage from other degradation mechanisms that have been detected in the SGs that have the potential to result in accident-induced leakage for evaluation against the accident-induced leakage performance criteria.

13. *The proposed "modified B*" approach relies to some extent on an assumed, constant value of loss coefficient, based on a lower bound of the data. This contrasts with the "nominal B*" approach which, in its latest form (as we understand it) is not directly impacted by the assumed value of loss coefficient since this value is assumed to be constant with increasing contact pressure between the tube and tubesheet. Given the amount of time for the NRC staff to review the interim ARC, the NRC staff will not be able to make a conclusion as to whether the assumed value of loss coefficient in the "modified B*" approach is conservative. However, the NRC staff has performed some evaluations regarding the potential for the normal operating leak rate to increase under steam line break conditions using various values of* (l_{NOP}/l_{SLB}) *determined from the "nominal B*" approach (which does not rely on an assumed value of loss coefficient). With*

these analyses and recognizing the issues associated with some of these previous H/B* analyses, it would appear that a factor of 2.5 reasonably bounds the potential increase in leakage that would be realized in going from normal operating to steam line break conditions. Discuss your plans to modify your proposal to indicate that the leak rate during normal operation (for flaws in the lower 4-inches of tube) will increase by a factor of 2.5 under steam line break conditions.*

[The NRC staff makes two observations here in response to possible industry concerns regarding Item 11. First, the NRC staff acknowledges that the ratio of the allowed accident leakage and the operational leakage is only 2.5 for Wolf Creek, which is equal to the factor of 2.5 above. (This ratio is 3.5 for Vogtle and 5 for Byron/Braidwood). This is not an atypical situation as is discussed in NRC RIS 2007-20. The operational leakage limit in the technical specifications can never be assumed to ensure that accident leakage will be within what is assumed in the accident analysis, even if the technical specification limit is zero. For example, part through wall flaws in the free span which are not leaking under normal operating conditions may pop through wall and leak under accident conditions. For cracks in the free span which are leaking under normal operating conditions, the ratio of SLB leakage to normal operating leakage can be substantially greater than 2.5 depending on the length of the crack. It is the licensee's responsibility to ensure that the accident leakage limits are met through implementation of an effective SG program, including an engineering assessment of any operational leakage that may occur in terms of its implications for leakage under accident conditions (based on considerations such as past inspection results and operational assessments, experience at similar plants, etc.).

Second, the NRC staff is not aware of any operational leakage to date from the tubesheet region for the subject class of plants, and there seems little reason to expect that this situation will change significantly in the next 18 months. Thus, the NRC staff's approach discussed above is not expected to have any significant impact for the licensees requesting relief from the tube repair criteria in the lower 4-inches of the tube.]

Response:

The proposed ratio of 2.5 of the SLB to NOP leakage is conservative from the perspective of predicted SLB leak rate from a postulated flaw below TTS-17 inches based on the analysis below. Based on the D'Arcy Model for flow in an axial porous medium, if no value for loss coefficient is assumed, the increase in predicted leakage from the tubesheet region would be lower than that determined by using a factor of 2.5 and also than that provided in the IARC justification.

For example, assume that both the loss coefficient and the length of porous medium surrounding a tube above a postulated crack are constant during both normal operating (NOP) and steam line break (SLB) conditions. The crevice below the neutral axis of the tubesheet will be tighter during accident conditions even if no credit is taken for thermal lockup between the tube and the tubesheet due to increased pressure differential across the tube. If the pressure differential across the tube at SLB conditions is discounted, the resulting condition is still an increase in contact pressure due to structural deflections and rotations. Thus, there is no basis to assume a lower loss coefficient at SLB condition than at NOP condition. Further, the viscosity during a SLB accident would be higher, due to the reduced temperatures in the crevice. Therefore, the assumption of a constant value for loss coefficient is, in fact, the worst case, and is reasonable and conservative for the IARC because the flow resistance is expected to increase during a postulated SLB event below 17 inches from the top of the tubesheet.

Following the assumptions described in Question 13 (above), the D'Arcy Model becomes:

$$Q = \frac{\Delta p}{R}$$
$$R = \mu K l$$

$$K_{NOP} = K_{SLB} = K$$

$$l_{NOP} = l_{SLB} = 17 \text{ in} = l$$

This assumption forces the estimated increase in leakage to be a factor based on the ratio of differential pressures and the ratio of the applicable viscosities only. For the Wolf Creek steam generators, the viscosity of the fluid during NOP conditions is approximately 1.75×10^{-6} lbf-sec/in^2 and during SLB is approximately 2.66×10^{-6} lbf-sec/in^2. The pressure differential ($\Delta p = P_{PRI} - P_{SEC}$) for Wolf Creek during NOP is 1443 psig and the pressure differential during SLB is 2560 psig. Substitution of these values into the D'Arcy Model gives,

$$Q_{SLB} = \frac{2560}{2.66e-6(Kl)} = 9.624e8 / Kl$$

$$Q_{NOP} = \frac{1443}{1.75e-6(Kl)} = 8.245e8 / Kl$$

$$\frac{Q_{SLB}}{Q_{NOP}} = \frac{9.624e8}{8.245e8}\frac{Kl}{Kl} = \frac{9.624e8}{8.245e8}\frac{1}{1} = \frac{9.624e8}{8.245e8} = 1.167$$

Using the D'Arcy Model to calculate the estimated increase in leakage during SLB yields a result of approximately 1.17. This is less than the conservative ratios which range from 2 to 6 as reported in the IARC description and the 2.5 factor proposed by the NRC staff.

For integrity assessments, the ratio of 2.5 will be used in the completion of both the condition monitoring (CM) and operational assessment (OA) upon implementation of the IARC. For example, for the CM assessment, the component of leakage from the lower 4 inches for the most limiting steam generator during the prior cycle of operation will be multiplied by a factor of 2.5 and added to the total leakage from any other source and compared to the allowable accident analysis leakage assumption. For the OA, the difference in leakage from the allowable limit during the limiting design basis accident minus the leakage from the other sources will be divided by 2.5 and compared to the observed leakage. An administrative limit will be established to not exceed the calculated value.

It is not planned to modify the existing IARC report, but, as noted above, a constant multiplier of 2.5 will be used in CM and OA evaluations to calculate SLB leakage from the lower 4 inches.

14. The mathematical constant π has been omitted from the first term of the equation near the top of page 8 and the equation at the bottom of page 8. It is not clear if this is a typographical error, or if π has been purposefully omitted. If the omission is intentional, please explain.

Response:

Two typographical errors have been identified in the left hand side of the equations for force balance for the partial circumferential flaw in the steam generator tube wall and the partially circumferential, through-wall flaw in the steam generator tube wall on Page 8 of LTR-CDME-08-11 P-Attachment. A factor of π was omitted in each equation in the report but not in the actual calculations. The calculation results are not affected by the typographical errors.

15. The last term of the equation at the bottom of page 8 includes the parenthetical $(r_o^2+r_i^2)$. The staff believes that this should be $(r_o^2-r_i^2)$. It is not clear if this is a typographical error, or if the radii are intentionally being summed. If intentional, please explain why the squared radii should be summed and not subtracted.

Response:

Westinghouse agrees that the plus sign (+) should indeed be a minus sign (-). The error is typographical and did not affect the calculations. The last term in the force balance equation for the partially circumferential, through-wall flaw in the steam generator tube contains a $\sigma \times (1/2) \times (r_o^2 + r_i^2) \times \Delta\theta$ term on the right hand side of the equation. That should read $\sigma \times (1/2) \times (r_o^2 - r_i^2) \times \Delta\theta$.

16. Explain why it is necessary to subtract A_f (area of the flaw) from S.A. (surface area of the frustum) in the first term of the force balance on page 10. (The staff believes that this term should be deleted.)

Response:

The area of the flaw must be subtracted from the surface area of the frustum when calculating the force balance because that area is no longer contiguous and cannot react to the applied stress. In other words, the flaw area is no longer available to the principal stress, but, is instead loaded by the internal pressure.

17. Explain the use of the mathematical constant P_i (internal pressure) rather than P ($3\Delta P$ or 4800 psi) on the equations on pages 8 and 10. The explanation on page 11 is not sufficient and appears to the staff to be incorrect.

Response:

It remains Westinghouse's position that it is conservative and correct to use an internal pressure of 2250 psi on the crack flank to calculate an acceptable remaining ligament for crack-like indications that may be present in the tube and weld. However, at the NRC staff's request, the allowable ligament sizes for the tube and the weld were recalculated assuming a 4800 psi differential pressure on the crack flank. The revised values for remaining ligament for the tube and the weld are [ ]^{a,c,e} (including an adjustment for growth) respectively.

For completeness, a summary of the Westinghouse position on the justification for the use of an internal pressure of 2250 psi is provided below.

A SG tube is a thick-wall cylinder. This is consistent with the ASME Code stress analysis of the steam generator tubing. Roark (Reference 8) defines a thin-wall cylinder as a cylinder with an inside radius to thickness ratio (R/t) greater than 10. For the Model F tube, R/t = 8.8, therefore, the tube is considered a thick-wall cylinder.

Reference 9 provides the equation of axial stress in the thick wall cylinder as:

$$\sigma_{zz} = \frac{p_1 a^2 - p_2 b^2}{b^2 - a^2} + \frac{P}{\pi\left(b^2 - a^2\right)}$$

Where P is an active external load (for this case = 0)

p_1 is the internal pressure

p_2 is the external pressure

a is the inside radius

b is the outside radius

The second term in the equation, $\frac{P}{\pi\left(b^2 - a^2\right)}$, goes to zero because the applied external load in this case is zero.

The equation is conservatively simplified by assuming the $p_2 b^2$term is negligible. Making this assumption conservative since retaining the term would reduce the axial calculated stress σ_{zz}.

The equation is reduced to let p_1 equal the pressure differential Δp. This is consistent with the equation in example 11.2 of Reference 9. This equation, and the following limitations, are echoed in Roark (Reference 8) Table 13.5, Case 1.b. The final equation for the calculation of stress due to the end cap load becomes

$$\sigma_{zz} = \frac{\Delta p a^2}{b^2 - a^2}$$

Calculation of the end cap load using this form of the equation is inherently conservative.

The limitation of the equation for axial stress in the thick-wall cylinder due to end cap load, and for the stress equations in the cylinder, is that the section of interest is far removed from the end caps (Reference 9). Consequently, the stress in the degraded section of the cylinder is increased by the reduced wall, but the end cap load remains constant. Calculating the end cap load for the thick-wall cylinder using the degraded wall thickness is equivalent to assuming that the wall thickness for the entire tube is the same as for the degraded local section.

It is the Westinghouse position that the load on the crack flank should be calculated separately from the end cap load. This is based on the fact that the end cap load already takes into account any variation in the cross section of the tube.

The underlying assumption for the IARC is that all circumferential cracks detected are 100% through wall over the entire indicated length. The Westinghouse crevice pressure test data (Reference 10) shows that the pressure in the crevice external to the tube in the immediate area of the penetration is the same as the internal pressure; therefore, there is no differential pressure at that location and $3\Delta p$ equals zero. The existing analysis conservatively applies the entire primary side pressure to the crack face. There is no operating condition that justifies using triple the primary pressure differential on the crack face and the required safety by the ASME Code for this situation (classification as secondary stress) would imply a safety factor of 1.0 on any primary side pressure.

Finally, the stresses calculated on the degraded section are compared to the flow stress which is very conservative for this situation. The condition of interest is one of pure axial separation under the assumption of the IARC, i.e., no axial friction forces between the tube and the tubesheet, but the tubesheet is present in close contact to prevent bending forces. For pure axial separation, it is appropriate to use the ultimate strength of the material, since no bending can occur and burst is not possible due to the constraint provided by the tubesheet.

References:

1. NEI-97-06, Rev. 2, "Steam Generator Program Guidelines," May 2005.

2. LTR-CDME-08-11, "Interim Alternate Repair Criterion (ARC) for Cracks in the Lower Region of the Tubesheet Expansion Zone," January 31, 2008.

3. WCAP-12522, "Inconel Alloy 600 Tubing-Material Burst and Strength Properties," January 1990.

4. WNET-180 (Proprietary), Volume 11, Rev. 0, "Model F Steam Generator Stress Report," Westinghouse Electric, Pittsburgh, PA, September 1980.

5. LTR-CDME-05-209-P, "Steam Generator Tube Alternate Repair Criteria for the Portion of the Tube Within the Tubesheet at the Wolf Creek Generating Station," January 2006.

6. WNET-153 (Proprietary), Volume 6, Rev. 0, "Model D5 Steam Generator Stress Report," Westinghouse Electric, Pittsburgh, PA, December 1981.

7. CN-CDME-08-4, Rev. 1, "Structural Evaluation of the Minimum Circumferential Ligament Required as Part of the WCNOC IARC," March 2008.

8. Roark's Formulas for Stress and Strain, Warren C. Young and Richard G. Budynas, Seventh Edition, McGraw-Hill, 2002.

9. Advanced Mechanics of Materials, Arthur P. Boresi and Richard J. Schmidt, Sixth Edition, John Wiley and Sons, 2003.

10. STD-MC-06-11-P, Rev. 1, "Pressure Profile Measurements During Tube-to-Tubesheet Leakage Tests of Hydraulically Expanded Steam Generator Tubing," August 30, 2007.