October 15, 2006

MEMORANDUM TO: Harold K. Chernoff, Chief
Plant Licensing Branch I-2
Division of Operating Reactor Licensing
Office of Nuclear Reactor Regulation

FROM: G. Edward Miller, Project Manager **/RA/**
Plant Licensing Branch I-2
Division of Operating Reactor Licensing
Office of Nuclear Reactor Regulation

SUBJECT: SEABROOK STATION, UNIT NO. 1 - RECEIPT OF DRAFT RESPONSE FROM LICENSEE CLARIFYING QUESTIONS RELATED TO A REQUEST FOR ADDITIONAL INFORMATION (TAC NO. MC8554)

The enclosed draft request for additional information (RAI) response was received via e-mail on September 7, 2006, from Mr. Russell Lieder, FPL Energy Seabrook, LLC (FPLE). This draft RAI response was transmitted to facilitate the technical review being conducted by the Nuclear Regulatory Commission (NRC) staff and to support a conference call with FPLE in order to clarify certain items in FPLE's letter dated August 8, 2006. The draft RAI response is related to FPLE's submittal dated September 29, 2005, regarding the limited inspection of the steam generator tube portion within the tube sheet. Transmittal of the draft RAI response allowed for a more concise discussion between FPLE and the NRC staff. The enclosed draft RAI does not represent a formal response from FPLE to NRC staff questions.

Docket No. 50-443

Enclosure:
Draft RAI response

October 15, 2006

MEMORANDUM TO: Harold K. Chernoff, Chief
Plant Licensing Branch I-2
Division of Operating Reactor Licensing
Office of Nuclear Reactor Regulation

FROM: G. Edward Miller, Project Manager **/RA/**
Plant Licensing Branch I-2
Division of Operating Reactor Licensing
Office of Nuclear Reactor Regulation

SUBJECT: SEABROOK STATION, UNIT NO. 1 - RECEIPT OF DRAFT RESPONSE FROM LICENSEE CLARIFYING QUESTIONS RELATED TO A REQUEST FOR ADDITIONAL INFORMATION (TAC NO. MC8554)

The enclosed draft request for additional information (RAI) response was received via e-mail on September 7, 2006, from Mr. Russell Lieder, FPL Energy Seabrook, LLC (FPLE). This draft RAI response was transmitted to facilitate the technical review being conducted by the Nuclear Regulatory Commission (NRC) staff and to support a conference call with FPLE in order to clarify certain items in FPLE's letter dated August 8, 2006. The draft RAI response is related to FPLE's submittal dated September 29, 2005, regarding the limited inspection of the steam generator tube portion within the tube sheet. Transmittal of the draft RAI response allowed for a more concise discussion between FPLE and the NRC staff. The enclosed draft RAI does not represent a formal response from FPLE to NRC staff questions.

Docket No. 50-443

Enclosure:
Draft RAI response

DISTRIBUTION:
Public
RidsNrrPMVNerses
EMurphy
LPL1-2 R/F
RidsNrrPMGMiller
RidsNrrDorlLplb

Accession No.: **ML062650257**

OFFICE	LPL1-2/PM	LPL1-2/LA	LPL1-2/BC
NAME	GEMiller:osr	CRaynor	HChernoff
DATE	9/21/06	10/11/06	10/15/2006

OFFICIAL RECORD COPY

DRAFT

DRAFT RESPONSES TO POTENTIAL SEABROOK RAI
SEPTEMBER 6, 2006

The following is a draft of responses to the draft RAI received by FPL Energy-Seabrook from the NRC on 8-29-06 via an e-mail from Mr. G.E. Miller (USNRC) to Mr. M. O'Keefe (Seabrook). These draft responses are provided to facilitate discussions between the NRC reviewer and the licensee to clarify the prior response to an NRC RAI. Each question is reproduced followed by the response to the question.

The following references are referred to in the draft questions. References that apply to the draft responses follow each response.

Question References:

1. FPL Energy Seabrook, LLC letter SBK-L-05186, Proprietary Information to Support Licensee Amendment Request 05-08, "Limited Inspection of the Steam Generator Tube Portion Within the Tubesheet," dated September 29, 2005."

2. FPL Energy Seabrook, LLC letter SBK-L-06157, Response to Request for Additional Information Regarding License Amendment Request 05-08, "Limited Inspection of the Steam Generator Tube Portion within the Tube Sheet," dated August 8, 2006

3. Wolf Creek Nuclear Operating Company letter no. ET 06-0004, "Revision to Technical Specification 5.5.9, Steam Generator Tube Surveillance Program," dated February 21, 2006 (ML060600456).

1. In FPL Energy Response 4 in Reference 2, it is stated that WCAP 16053, Rev 1 provides no additional details other than those already included in the technical attachment to Reference 1 and that the information from WCAP 16053 required for the technical justification attached to Reference 1 is included as part of that technical justification. However, in FPL Energy Response 5 under the heading, "Analysis of Circumferential Cracking," it is stated that details of the circumferential crack model are contained in WCAP 16053 and the EPRI Steam Generator Degradation Specific Flaw Handbook. Please provide a description of the circumferential crack model, including the assumed loads acting on the cracked cross section and how these loads were determined.

Response:

The response to original RAI #4 provided an explanation why WCAP 16053 was not necessary for the review; however, it was overlooked that this WCAP was referenced in the response to question #5. WCAP-16053 has the same content as WCAP-15932, Rev. 1 which, as noted in original question #9, was submitted on the Callaway docket, NRC Accession No. ML022910436. Appendices B and C of WCAP-15932, Rev. 1 describe the circumferential crack model, the loads acting on the crack cross-section and how these loads were determined.

2. Page 76 of 127 of the technical attachment to Reference 3 above (LTR-CDME-05-209-P) for Wolf Creek identifies that the main loadings on a circ crack below the H distance are the pressure loads acting on the crack face. It is also stated here that the internal pressure end cap load are not transmitted below about 1/3 the H* distance. Assuming that H* is determined correctly, the staff agrees that this statement is true for normal operating pressure provided the tube is severed immediately below the 1/3 H* distance. Similarly, the 3 delta P end cap load does not extend below the full H* distance assuming the tube is severed immediately below the H* distance. If the tube is not severed, then much of the end cap load will be transmitted below the H* distance. The calculated H* distance is based in part on pull out tests (on specimens that were basically severed at the bottom) where the pull out criterion was an axial displacement of 0.25 inches at the bottom of the specimen. If the tube is intact below the H* distance, then the tube must be able to stretch by 0.25 inches between the weld and the H* location which means there must be considerable force transmitted below the H* distance. The tube to tubesheet joint (where the tube is not severed inside the tubesheet) is a redundant structure. Has a detailed analysis (e.g., finite element analysis) been performed to determine how much of the full internal pressure end cap load is actually transmitted to the cracked cross section under normal operating and accident conditions? If so, describe the analysis and the results. If not, how was the portion of the full internal pressure end cap load actually transmitted to the cracked cross section determined?*

Response:

A detailed analysis, other than described in the technical justification to support the Seabrook License Amendment Request, has not been completed by Westinghouse to determine how much of the full internal pressure end cap load, if any, may be transmitted to a cracked tube cross-section at or below the H*distance. The reasons follow:

a. The results of pull out tests coupled with those from finite element evaluations of the effects of temperature and primary-to-secondary pressure on the tubesheet interface loads have been used to demonstrate that an engagement length of less than or equal to 8.46 inches is sufficient to equilibrate axial loads resulting from the consideration of 3 times the normal operating pressure and 1.4 times the limiting accident pressure differences for the Seabrook steam generators. The current license amendment request for the Seabrook steam generators is a 17 inch permanent inspection length criterion that contains ample structural margin to the full internal end cap load.

b. Actual internal pressure end cap loads are not transmitted below about 1/3 of the H* depth based on the calculation documented in the technical justification. The only source of forces acting to extend a circumferential crack at or below the H* distance is the primary pressure acting on the crack flanks.

In addition, since the tube is captured within the tubesheet, there are additional forces acting to resist the opening of a crack. The contact pressure between the tube and tubesheet results in a friction induced shear force acting to oppose the direction of cracking opening, and the force on the crack flanks is compressive on the material adjacent to the plane of the cracks. Hence, Poisson's ratio radial expansion of the tube material in the immediate vicinity of the crack plane is induced that acts to restrain the opening of the crack. Further, the differential thermal expansion of the A600TT tube is greater than that of the carbon steel tubesheet, thereby inducing a compressive stress in the tube below the H* length.

c. The value for tube pullout strength of 118 lbf/inch used in the H* analysis included in LTR-CDME-05-170-P for Seabrook is extremely conservative as it is based on first slip pullout strength test results.

   The Theory of Elasticity model was used to calculate the contact pressure preload associated with the test pullout forces. This value was then converted to an average force per unit length using the gross area of contact. Also, a conservative coefficient of friction (μ) of 0.3 was used because it results in a smaller value for residual contact pressure. Then, a conservatively lower value of μ of 0.2 was used in determining the pullout strength of 522.3 lbf/in (for a Model F SG) because the lower value of μ results in a smaller value for pullout strength. Lastly, the 522.3 lbf/in pull out strength value is the lower 95%, one sided confidence limit value.

d. If the expansion joint were not present, there would be no effect on the pullout strength if axial cracks are present above/below the H* distance. Tubes with circumferential cracks greater than 180° by 100% deep would have sufficient strength to carry the pullout forces without tube separation.

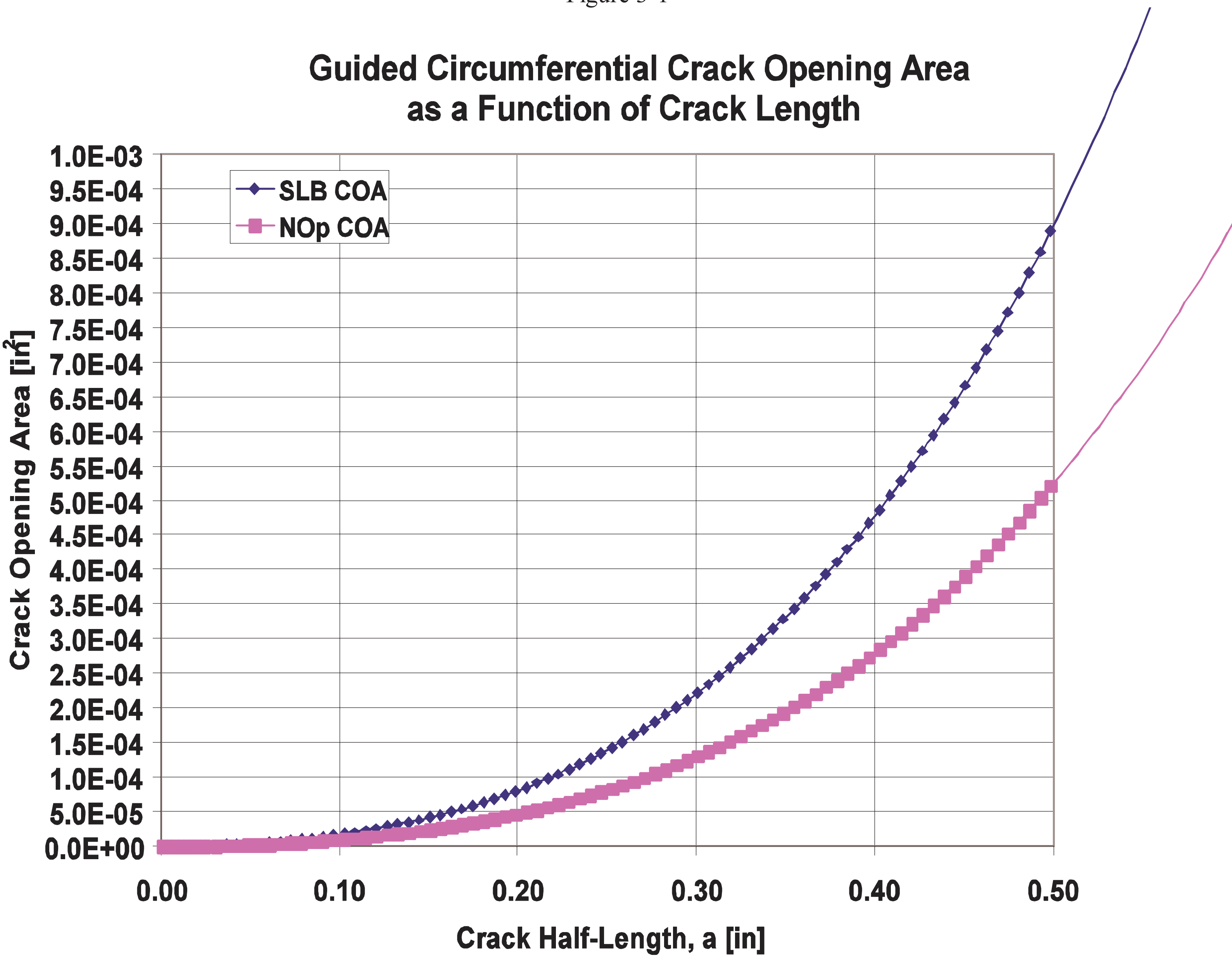
e. The proposed 17 inch inspection length is well below the length of engagement needed for the leak rate during a postulated steam line break event to be bounded by twice the leakage experienced during normal operating conditions.

3. Regarding FPL Energy Response 5 in Reference 2, provide a plot of COA for circumferential cracks located 4 inches from the bottom of the tubesheet as a function of crack length and tubesheet radius location for normal operating and main steam line break conditions. Provide a plot of leak rate as function of the same parameters, neglecting the effect of crevice resistance.

Response:

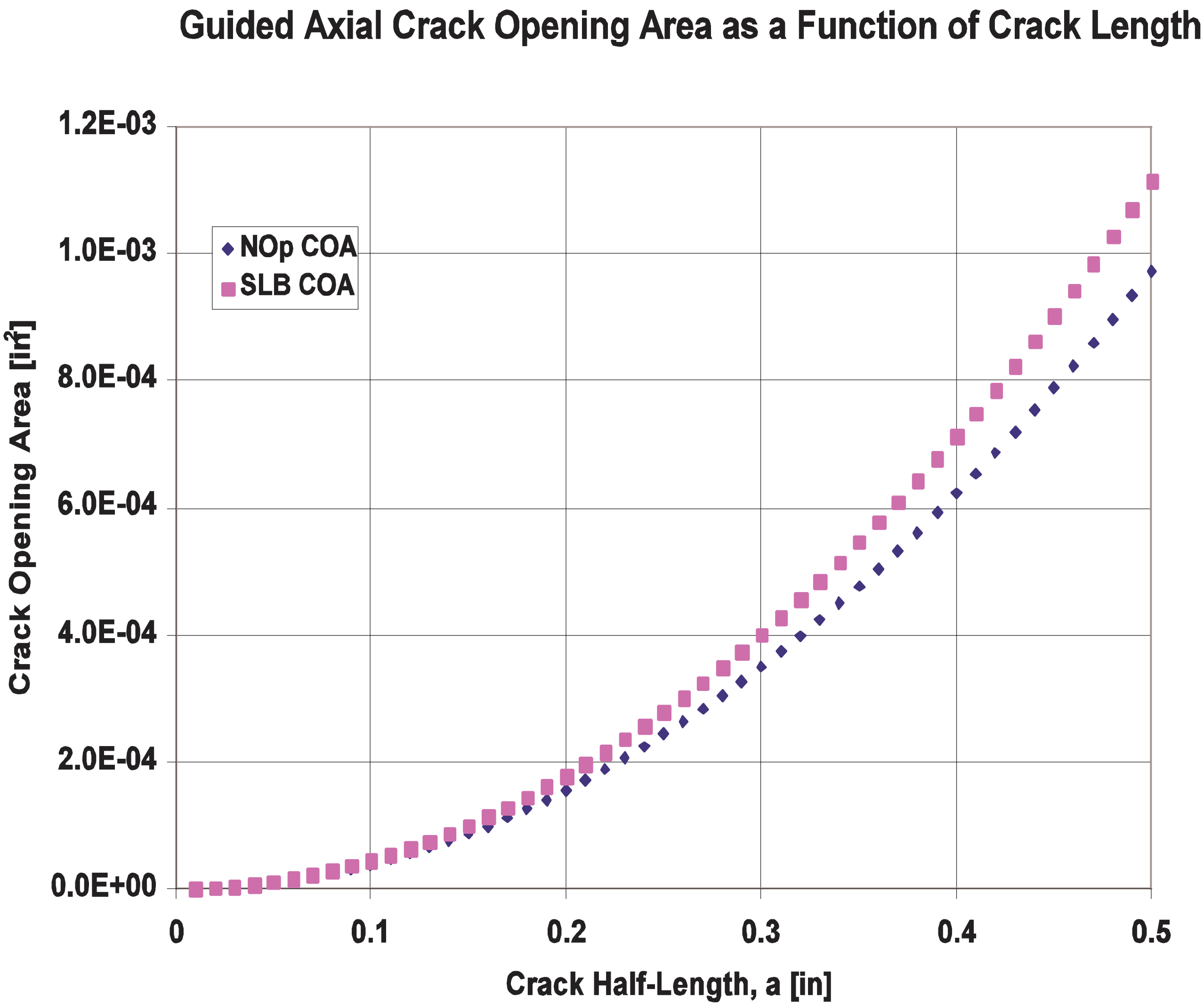
As requested, Figure 3-1 and Figure 3-2 are plots of the circumferential and axial crack opening area, as a function of crack length, for both the normal operating (NOp) and main steam line break (SLB) conditions. The crack opening area models are independent of tubesheet radius. The results shown below include the more conservative estimates for a crack above the H* elevation (i.e., worst case) in the tubesheet.

Figure 3-1


Guided Circumferential Crack Opening Area
as a Function of Crack Length
SLB COA
NOp COA
Crack Opening Area [in2]
1.0E-03
9.5E-04
9.0E-04
8.5E-04
8.0E-04
7.5E-04
7.0E-04
6.5E-04
6.0E-04
5.5E-04
5.0E-04
4.5E-04
4.0E-04
3.5E-04
3.0E-04
2.5E-04
2.0E-04
1.5E-04
1.0E-04
5.0E-05
0.0E+00
0.00
0.10
0.20
0.30
0.40
0.50
Crack Half-Length, a [in]

DRAFT

Figure 3-2



Plots of the leak rate, as determined by the application of the D'Arcy equation for flow in a porous medium, due to circumferential crack resistance only, at the near, mid and peripheral tubesheet radius locations (at an elevation of 17.03 in below the top of the tubesheet) are provided below on Figures 3-3 and 3-4 for the NOp and SLB conditions. . In each plot, the terms "near", "mid" and "peripheral" refer to TS radii. The tubesheet radii for each range are: Near (2.0774 In), Mid (33.101 In), Peripheral (60.2475 In).

DRAFT

Figure 3-3

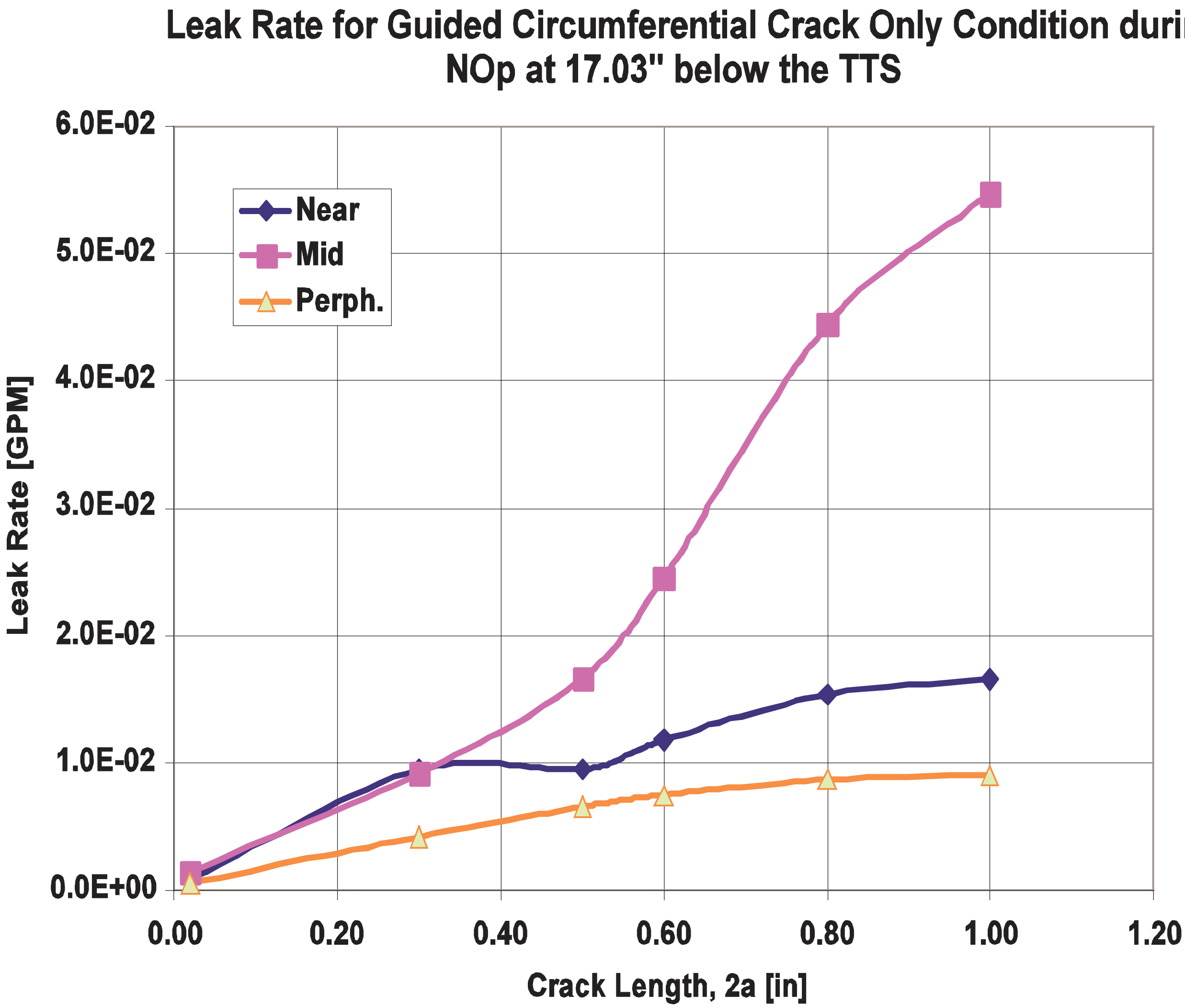
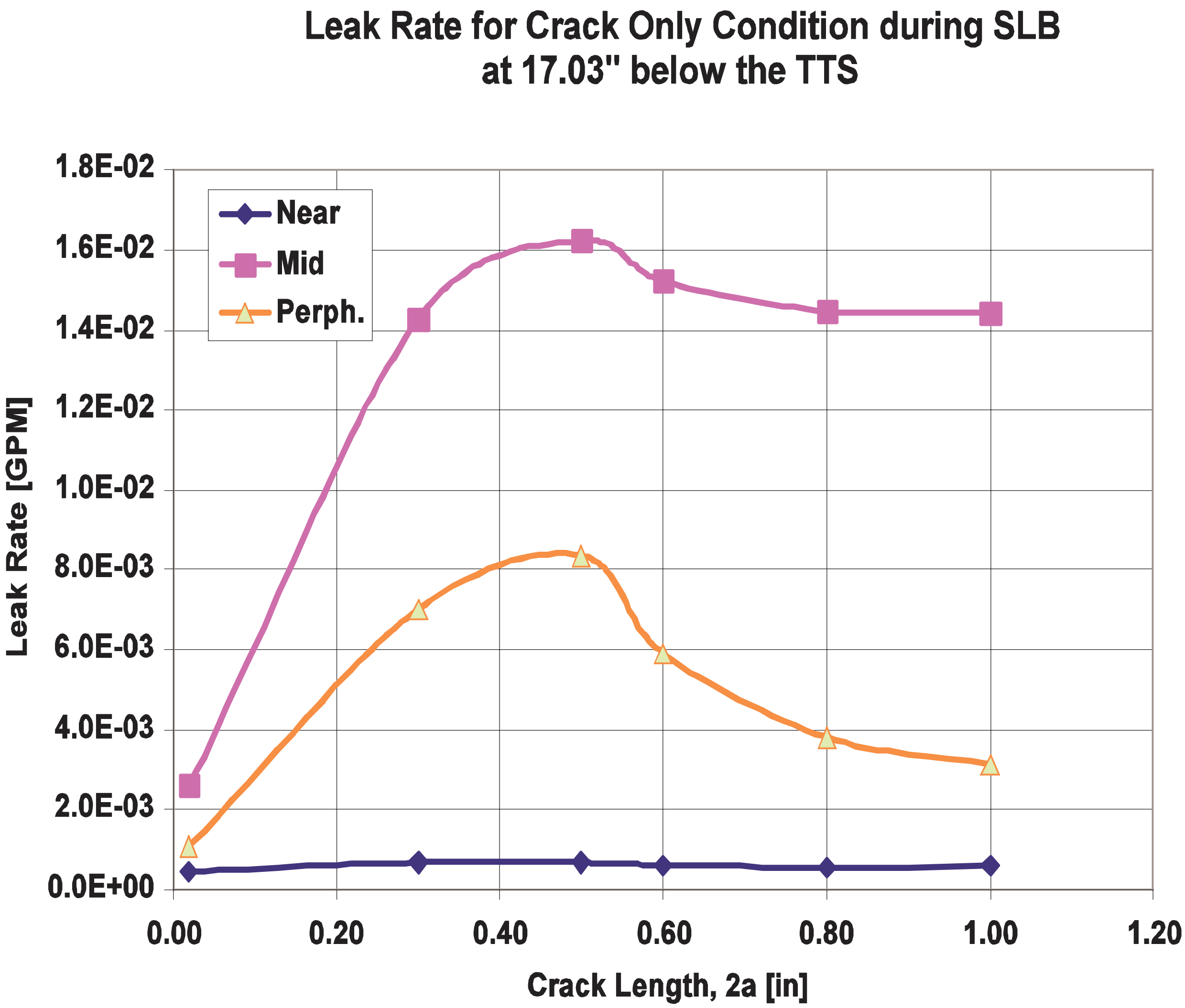


Figure 3-4


Leak Rate for Crack Only Condition during SLB
at 17.03" below the TTS
Near
Mid
Perph.
1.8E-02
1.6E-02
1.4E-02
1.2E-02
1.0E-02
8.0E-03
6.0E-03
4.0E-03
2.0E-03
0.0E+00
Leak Rate [GPM]
0.00
0.20
0.40
0.60
0.80
1.00
1.20
Crack Length, 2a [in]

4. Regarding FPL Energy Response 5 in Reference 2, provide revised versions of Figures 3 and 4 to include the leak rate ratios for cracks in the range of 0.1 to 0.5 inches in length. It would seem from Figures 3 and 4 that if crack resistance dominates crevice resistance, then leakage ratios may exceed 2 for through wall crack lengths less than 0.5 inches for tubes near the periphery of the bundle, particularly for circumferential cracks. Also, provide similar figures for the near radius and mid radius locations.

5. The discussion accompanying Figures 3 and 4 states that cracks less than 0.5 inches in length are not expected to cause any "relative significant leakage." Please explain the basis for concluding the leakage contribution from population of circumferential cracks of through-wall length less than 0.5 inches is small relative to the leakage contribution from the population of through-wall cracks greater than 0.5 inches in length such that the leakage ratio between normal operating and accident conditions is dominated by the leakage ratio (which is less than 2) exhibited by the population of cracks larger than 0.5 inches. This explanation should consider any relevant operating experience regarding the probability density function of 100% through wall crack lengths and, in addition, the plots provided in response to question 2 above.

Response (to RAI 4 and 5):

Test data have demonstrated that the resistance per unit length is a monotonically increasing, non-linear function of the contact pressure. The deflection of the tubesheet in combination with an increase in internal pressure results in a change in contact pressure being 0 between normal operating conditions and steam line break conditions at some distance below the top of the tubesheet that is above the neutral surface of the tubesheet, designated as the B* distance. Using this elevation as a reference, the increase in resistance per unit length below the zero change location must always be more than the absolute value of the decrease in resistance per unit length above the 0 change elevation. Thus, the average resistance in going from normal operation to SLB must increase and the average leak rate must decrease. This is independent of the individual crack leak rates involved (and crack orientation) and only depends on the trend. The maximum volumetric leak rate, Q, is based on Darcy's model for flow through a porous media and is a function of the driving pressure, the inverse values of viscosity, the loss coefficient and the length crevice. Below the B* distance, the maximum leak rate from all indications would be limited to less than 2 times the leakage during normal operating conditions. It has already been pointed out by the NRC staff that the use of Darcy's formula is conservative relative to alternative models such as Bernoulli or orifice models, which assume leak rate to be proportional to the square root of differential pressure.

The concern with the appearance of the ratio plots is an artifact of the small numbers involved when calculating leak rates through small (less than 0.5 inch) cracks using the available models. For example, the hydraulic radius of the crack appears in the numerator of leak calculations. This value is calculated by dividing the crack opening area by the wetted perimeter of the crack, which is conservatively approximated for tube cracks within the tubesheet as twice the crack opening displacement plus twice the crack length. Because the hydraulic radius, and other geometric size-related variables, are smaller for the NOp condition than the SLB condition, taking the ratio of the two results in dividing an already small number by a much smaller number and thus a larger leak rate ratio.

The available data for the population of circumferential cracks is from the 1999 inspection record of the circumferential cracks at Callaway Unit 1 in mill-annealed Alloy 600 tubing. The total data set included 40 circumferential cracks observed, with an average crack angle of 40.18º $\pm$ 21.62º, a maximum angle of 108º and a minimum angle of 20 º. Of these 87.5% of the cracks were less than 0.5 inch in arc length, with 75% of the circumferential cracks covering an angle of 40º or less. In the data set, 25% of the circumferential cracks were 95% through wall or greater, with a single crack being identified as 100% through wall. The through wall indications are consistent with the available data from prior inspections and suggest very little change in the through wall crack depth.

For a tube with a 0.6875 inch outer diameter (Model F SG), a crack angle of 40º corresponds to a crack arc length of approximately 0.24 inches. The predicted crack opening area for a guided circumferential crack (constrained from bending, crack opening constrained) 0.24 inch in length, using the models described in WCAP-15932 (Callaway docket, NRC Accession No. ML022910436) and shown in the plots above, is approximately 1.51e-5 in^2 for the NOp condition and 2.57e-5 in^2 for the SLB condition. It is reasonable to consider a value on the order of 10^{-5} in^2 as negligible for the purposes of crack opening area.

This result is consistent with the values for crack opening area that can be calculated using the alternate approach of calculating the kink angle compatibility via methods described in The Stress Analysis of Cracks Handbook (2nd Edition) by Tada. The result of such calculations, given in case 33.1 and 33.2 of the text, show that the crack opening area for circumferential cracks with an angle of less than 40º are not expected to have any crack opening area. Therefore, it is reasonable to exclude 75% of the cracks, having an angle of 40º or less, from further consideration in a leakage analysis.

The probability of a crack from the available data set having an angle of 40º or greater, but still less than 0.5 inch in arc length, is 12.5%. The probability of a circumferential crack having an arc length of 0.5 inch or greater is also 12.5%. Given that it is not possible for a crack less than 0.5 inch in length to have a greater crack opening area, and therefore available flow area for a leak, than a crack larger than 0.5 inch in length, it is reasonable to assume that the magnitude of leakage from cracks < 0.5" long is less than the magnitude of leakage from cracks longer than 0.5" during normal operating conditions. The magnitude of leakage peaks at 0.5" for the crack only condition at SLB conditions.

6. *There is a statement in the discussion underneath Figure 4 that reads, "The results from the crack-only analyses show that in the absence of the dent the resistance to flow is increased and each crack type produces a lower leak rate ratio." Please clarify how denting relates to these analyses. Additionally, please qualify what the increase in resistance to flow and lower leak ratios are relative to.*

Response:

Please substitute the word "crevice" for the word "dent" in the discussion underneath Figure 4 in the original Seabrook RAI response.

The statement "the resistance to flow is increased and each crack type produces a lower leak rate ratio" was taken from the paragraph in the original Seabrook RAI response that said:

"The leak rate calculations of the values for cracks 0.50" and smaller should be discounted given the asymptotic nature of the equations. This is because such a small crack is equivalent to a point source or a singularity in the fluid flow equations and in reality is not likely to cause any significant leakage. The crevice only near radius leakage rate ratio results are also less than 2. **The results from the crack only analyses show that in the absence of the [crevice] the resistance to flow is increased and each crack type produces a lower leak rate ratio.**"

The above statement, shown in bold in the context of the original paragraph, was intended to qualify the observation that the leak rate ratio of a crack alone is decreased in the SLB condition relative to the NOp condition in the absence of a crevice due to the increase in the pressure drop across the crack.

7. *Historically, license amendments for alternate repair criteria have included, as a compensatory measure, revision of the technical specification operational primary to secondary leakage limits from 500 gallons per day (gpd) per SG and 1 gallon per minute for all SGs to a 150 gpd limit for each SG. You have proposed such a change as part of part of your requested technical specification amendment to incorporating TSTF-449 (Seabrook Station License Amendment Request 06-02, March 23, 2006, NRC Accession No. ML060870133). Given that the TSTF changes may not be approved prior to your scheduled refueling outage inspection, please describe your plans for implementing a 150 gpd operational limit pending the staff's approval of the TSTF amendment package.*

Response:

FPL Energy Seabrook limits operating primary to secondary leakage to 150 gallons per day in accordance with Operating Procedure OS 1227.02 "Steam Generator Tube Leak". The plant is required to shutdown upon reaching 150 gallons per day, which is more conservative than the current technical specification and meets the requirements in TSTF-449.

8. *Did any of the hydraulic expansions in Model D5 and F SGs experience a stress relief during fabrication, directly or indirectly (e.g., as a result of stress relieving the shell to tubesheet welds)? If so, how was this reflected in the pullout and leakages tests in support of the tubesheet amendment requests?*

Response:

The manufacturing sequence for a steam generator is such that the tube-to-tubesheet joint is completed prior to welding the channelhead to the tubesheet; therefore, the joint necessarily experiences a thermal cycle during the subsequent post weld heat treatment (PWHT) of the channelhead-to-tubesheet weld seam. The "soak temperature" for the weld seam is approximately 1150º F for a duration of 3 hours. In order to determine the temperatures which the expanded joints experience, thermocouples were installed in tubes in the region of the tubesheet during the manufacture of a steam generator prior to PWHT of the weld seam. The thermocouples were positioned in the steam generator tubes at depths corresponding to (1) the primary face of the tubesheet, (2) the secondary face of the tubesheet, and (3) about 8 inches beyond the secondary face of the tubesheet. As expected, the highest temperatures occurred at the periphery of the tube bundle on the primary side of the tubesheet with inboard temperatures rapidly dropping below 800ºF on the primary face. The peak temperatures observed were 1025ºF to 1050ºF at the primary side, 810ºF to 1060ºF at the secondary side and 641ºF at locations 8 inches beyond the secondary side of the tubesheet .

It is concluded in Reference 2 that the temperatures which the tubes experience are too low to effect stress relief of the tube transition zone residual stresses and are not expected to have any significant effect on joint tightness. The tubes may become discolored to some extent with a thin heat tint oxide film. Such heat tint films have been observed in various studies related to in-situ stress relief of U-bends and tube support plate intersections. In these studies Alloy 600 Row 1 U-bend tubes and reverse U-bend tubes were stress-relieved in air temperatures up to 1500ºF. Prolonged exposure of the various samples to representative reactor coolant chemistry and temperature or accelerated test conditions produced no evidence that the heat film is detrimental to the long term corrosion behavior of the tubing.

The Model D5, Model 44F, and 51F steam generator H* testing programs did, however, consider and implement a stress relief cycle on the tube-to-tubesheet test specimens. The thermocouple data discussed above implies that a soak temperature of 800ºF is justified and can be used for the test specimens. This is because the highest temperatures occur at the peripheral tube locations and the outboard holes experience compression at the secondary face during normal and faulted conditions, negating any potential effect of joint relaxation. A soak temperature of 900ºF for three hours was used for the tube pull and leak test specimens and is conservative for most tubes. The H* pullout and leakage testing for the Model F SGs has no indication that the stress relief was either considered or implemented in test specimen preparation.