ATTACHMENT (6)

Non-Proprietary -- Calculation No. CA04803, Revision No. 0,

"Flaw Growth Evaluation For

CCNPP Unit 2 Pressurizer Upper Level Tap Leak"

FORM 19, CALCULATION COVER SHEET

Form (Rev. 0)

INITIATION (Control Doc Type - DCALC)

DCALC No.: CA04803 REVISION NO.: 0000

VENDOR CALCULATION (CHECK ONE): ■ YES ☐ NO

RESPONSIBLE GROUP: Mechanical Engineering Unit

RESPONSIBLE ENGINEER: J.T. Conner

CALCULATION

ENGINEERING DISCIPLINE:

☐ Civil ☐ Instr & Controls ☐ Nuc Engrg
☐ Electrical ■ Mechanical ☐ Diesel Gen Project
☐ Life Cycle Mgmt ☐ Reliability Engrg ☐ Nuc Fuel Mgmt
☐ Other:

Title: Flaw Growth Evaluation for CCNPP Unit 2 Pressurizer Upper Level Tap Leak

Unit ☐ Unit 1 ☒ UNIT 2 ☐ COMMON

Proprietary or Safeguards Calculation ☐ YES ■ NO

Comments:

Vendor Calc No.: REVISION NO.:

Vendor Name: ~~bechtel power corporation~~ Hopper & Associates 7/30/99

Safety Class (Check one): ☒ SR ☐ AQ ☐ NSR

There are assumptions that require Verification during walkdown: AIT # N/A

This calculation **SUPERSEDES**: N/A

CALCULATION REVIEW:

RESPONSIBLE ENGINEER:	J.T. Conner	DATE:	7/30/1999
INDEPENDENT REVIEWER:	N/A	DATE:	
APPROVAL:	N/A	DATE	

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CALC. NO. CA04803
ATTACHMENT A
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POSTULATED FLAW FATIGUE GROWTH EVALUATION

FOR CCNPP UNIT 2 PRESSURIZER UPPER LEVEL TAP LEAK

Prepared for: Baltimore Gas and Electric Company
Calvert Cliffs Nuclear Power Plant
1650 Calvert Cliffs Parkway
Lusby, MD 20657

Prepared by: Hopper and Associates
300 Vista Del Mar
Redondo Beach, CA 90277

September 1998

BGE039

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TITLE: FLAW EVALUATION DATE: 9/8/98 PAGE: i

SUBJECT: TABLE OF CONTENTS BY: K.S. CK: MG SHT: i OF ii

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TITLE: FLAW EVALUATION DATE: 9/8/98 PAGE: ii

SUBJECT: TABLE OF CONTENTS BY: KS CK: MG SHT: ii OF ii

SECTION 1.0, 2.0, 3.0, 4.0 & 5.0

PREPARED BY: [signature] 9/9/98

REVIEWED BY: [signature] 9/9/98

SECTION 3.0

PREPARED BY: Robert E. Nickell 9/1/98

REVIEWED BY: [signature] 9/9/98

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ATTACHMENT A
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1.1 PROBLEM STATEMENT

A LEAK AT THE UNIT 2 PRESSURIZER INSTRUMENT NOZZLE WAS FOUND RECENTLY [1]. A FLAW WAS POSTULATED TO EXIST IN THE PRESSURIZER HEAD AND ASME BOILER AND PRESSURE VESSEL CODE, SECTION XI, APPENDIX A CALCULATIONS WERE PERFORMED [2]. THIS EVALUATION [2] CONSIDERED VARIOUS FLAW SIZES AND THE NUMBER OF CYCLES TO GROW TO A CRITICAL SIZE. THE PURPOSE OF THIS CALCULATION IS TO ASSUME A CRACK SIZE EQUAL TO THE CLADDING THICKNESS AT THE TIME THE REPLACEMENT NOZZLE WAS PUT INTO SERVICE, TO ESTIMATE THE CRACK SIZE TODAY BASED UPON THE NUMBER OF START-UP/SHUT-DOWN CYCLES, AND TO ESTIMATE THE NUMBER OF CYCLES REMAINING FOLLOWING THE

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PROCEDURES OF ASME B&PV CODE, SECTION XI, APPENDIX A. THE CONSERVATISM OF THIS APPROACH WILL ALSO BE CHECKED AND COMMENTED ON.

1.2 INVESTIGATION APPROACH

THE FRAMATOME TECHNOLOGIES CALCULATIONS WILL BE OBTAINED AND REVIEWED [2]. IT WILL BE ASSUMED THAT THE INPUT DATA FOR THE FRAMATOME TECHNOLOGIES CALCULATION IS CORRECT. THE METH-ODOLOGY AND RESULTS WILL BE EVALUATED FOR REASONABLENESS. HAND CALCULATIONS WILL BE PERFORMED TO ESTIMATE A CRACK SIZE TODAY BASED UPON A POSTULATED INITIAL FLAW IN THE CLADDING IN 1989 WHEN THE NOZZLE WAS REPLACED [3]. AND THE NUMBER OF START-UP/SHUTDOWN CYCLES SINCE 1989 [4]. AN ESTIMATE REMAINING LIFE WILL THEN BE CALCULATED USING AN UPPER SHELF K_{Ia} VALUE FROM THE OPEN LITERATURE. ADDITIONALLY, THE APPROP-RIATENESS OF USING THE SECTION XI,

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APPENDIX A APPROACH WILL BE CHECKED BY CONSIDERING PLASTICITY EFFECTS.

1.3 RESULTS SUMMARY

USING A SECTION XI, APPENDIX A APPROACH AND ASSUMING AN INITIAL (POSTULATED) FLAW SIZE EQUAL TO THE CLADDING THICKNESS, IT WAS FOUND THAT AN EXPECTED CRACK SIZE TODAY WOULD BE 0.151" WHICH COULD OPERATE FOR 14,650 MORE START-UP/SHUT-DOWN CYCLES BEFORE THE CRACK SIZE BECAME CRITICAL. IT WAS ALSO FOUND THAT SIGNIFICANT YIELDING WOULD OCCUR WHICH MAKES AN ELASTIC-PLASTIC ANALYSIS MORE APPROPRIATE. THIS IS NOT TO SAY THAT THE APPENDIX A APPROACH IS WRONG, ONLY CONSERVATIVE.

The FTI calculations[a] follow the procedures for evaluating detected and sized flaws in ferritic steel components with a thickness of four inches, or greater, as given in the ASME Code Section XI, Subsection IWB-3600. These procedures include the use of Appendix A to calculate flaw growth and the satisfaction of either IWB-3611 on flaw sizes or IWB-3612 on applied stress intensity.

Since the purpose of the calculations was not to evaluate a detected and sized flaw exceeding the limits contained in Table IWB-3510-1, but instead to evaluate the potential for a crack to extend into the upper head base metal from the Alloy 690 level tap instrument nozzle weld, the calculations are hypothetical. Appendix A procedures and IWB-3600 acceptance criteria are used only for illustration, and not to form the basis for a submittal to regulatory authorities on the evaluation of an actual flaw.

Because the postulated flaw is not a real flaw, the terminology of Appendix A and IWB-3600 is not followed precisely. The postulated initial flaw size is defined as a_i which, in the parlance of Section XI, is defined as the minimum critical flaw size of the (actual) flaw for initiation of nonarresting growth under postulated emergency and faulted conditions.

We will use the terminology a_o to denote the depth of the (postulated) flaw prior to evaluation.

The FTI calculations will then be described as examining four depths for a_o, ranging from a low of 15/16 inches (0.9375 inches) to a high value of 1.7106 inches. In the latter case, $a_o = a_c$, where a_c is defined as the minimum critical flaw size of the (postulated) flaw under normal operating conditions, as defined by a material fracture toughness of 63.25 ksi $\sqrt{in}$. If the actual material fracture toughness is used (at least 200 ksi $\sqrt{in}$ for temperatures greater than 180°F above the RT_{NDT} temperature), a_c is larger than the thickness of the upper head (3 7/8").

Because the material is operating on the upper shelf during the time when the internal pressure is applied, the flaw is actually governed by the provisions of IWB-3610(d)(2), rather than IWB-3610(d)(1), so that the primary stress limits of NB-3000 should be met based on the reduced area of the remaining ligament.

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TITLE: FLAW EVALUATION DATE: 8/31/98 PAGE: 7

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3.1 POSTULATED FLAW SIZE

The postulated flaw depth for the initial flaw should be based on plausible pre-service and early-life degradation mechanisms. For example, WCAP-2859 (Yankee Pressurizer Cladding Evaluation, November 2, 1965) reported [6] minor cracking in the spot-welded cladding of the upper head in the pressurizer at Yankee Rowe. The cracking was detected by dye penetrant examination and was attributed to high residual stresses and sensitized material that caused intergranular stress corrosion cracking (IGSCC). No indications were found in any weld-deposited cladding. The IGSCC of the spot-welded cladding was thought to be exacerbated by pickling that was used to remove contamination from the cladding surface.

WCAP-2859 argued successfully that growth of these IGSCC cracks from the cladding into the base metal, as the result of corrosion or hydrogen embrittlement, was not credible. Oxygen availability at the base of any cladding crack will be limited by diffusion, and hydrogen embrittlement by the low hydrogen concentration. Fatigue crack initiation and the potential for fast fracture were both considered, and found not to be an issue. However, the potential for crack growth for a through-clad crack was not evaluated, since the evaluation took place prior to the development of the ASME Section XI rules. More modern evaluation methods would be based on an initial flaw depth of 0.109 inches, the thickness of the Yankee Rowe pressurizer cladding.

The cladding thickness of the stainless steel weld overlay for the Calvert Cliffs pressurizer is specified to be a [redacted] 1/8-inch minimum (0.125 inches). A through-clad crack in this [redacted] overlay would be at least 0.125-inches deep, and could be slightly larger. For purposes of conservatism, the initial flaw size is postulated to be 0.150-inches deep, extending fully around the nozzle penetration.

It should be pointed out that IWB-3610(b)(1) stipulates that a Category 1 flaw that lies entirely in the cladding need not be evaluated. In this case, the initial flaw depth, a_0, is entirely in the cladding; however, cyclic crack growth will convert this surface flaw into a Category 2 flaw that requires the total flaw depth in both the ferritic steel and the cladding to be evaluated.

[It is noted that the FTI calculation stipulated a minimum flaw depth of 1 1/16 inches, which includes cladding thickness and complete lack of fusion in the weld butter and weld metal. For a flaw of such depth, some 2616 startup/shutdown cycles are needed to violate IWB-3612.]

<u>Cyclic Crack Growth</u>

$$\frac{da}{dN} = C_0 (\Delta K_I)^n ,$$

where $C_0 = 1.01 \times 10^{-7}$ inches/cycle; $n = 1.95$, $R = K_{max}/K_{min} \leq 0.25$.
This equation fits the portion of the curve in Fig. A-4300-1 (Reference Fatigue Crack Growth Curves for Carbon and Low Alloy Ferritic Steels Exposed to Water Environments) of Section XI, Appendix A, for $\Delta K_I \geq$ 13 [redacted] ksi$\sqrt{in}$ and $R \leq 0.25$.
This gives

$$a_f = \left[a_0^{(1-n/2)} + (1-n/2) C_0 \left(0.706 A_0 \sqrt{\pi}\right)^n N \right]^{1/(1-n/2)} ,$$

where a_f is the flaw depth after N cycles, a_0 is the initial flaw depth, and $A_0 = 37.5$ ksi. Then

$$a_f = \left[(0.150)^{0.025} + (0.025)(1.01 \times 10^{-7})(46.9257)^{1.95} N \right]^{40}$$

$$= \left[0.95368 + 4.586827 \times 10^{-6} N \right]^{40}$$

<u>N = 33 [redacted] cycles (from [4])</u>

[redacted]

A simpler calculation is to evaluate ΔK_I for a flaw depth of 0.150 inches.
Then

$$\Delta K_I = \sqrt{\pi a}\,(0.706) A_0 = 18.2 \text{ ksi}\sqrt{in}$$

Assume 19 ksi$\sqrt{in}$.
Then

$$\frac{da}{dN} = 1.01 \times 10^{-7} (19)^{1.95} = 3.147 \times 10^{-5} \text{ in/cycle},$$

so that, for 33 [redacted] cycles,

$$a_f = a_0 + 33\,[\text{redacted}]\,(3.147 \times 10^{-5} \text{ in/cycle}) = 0.151\,[\text{redacted}] \text{ inches}.$$

check ΔK_I = 18.2 [redacted] ksi$\sqrt{in}$.

o.k.

Therefore, we expect the flaw to grow 0.001 [redacted] inches into the base metal.
[For such small amounts of crack growth, we do not expect the NB-3000 allowables to need reconfirmation. Also, 19 ksi$\sqrt{in}$ satisfies IWB-3612.]

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3.2 REMAINING LIFE ESTIMATE

For $a_0 = 1\frac{1}{16}$ inches = 1.0625 inches

$$\Delta K_I = 48.37 \text{ ksi}\sqrt{in} \Rightarrow 1.1281 \text{ inches} \Rightarrow 49.84 \text{ ksi}\sqrt{in}$$

and

$$\frac{da}{dN} = 2.0635 \times 10^{-4} \text{ inches/cycle}$$

For 500 cycles, 0.1032 inches

$$\Rightarrow \Delta K_I = 50.66 \text{ ksi}\sqrt{in} \Rightarrow 1.0625 + 0.1032 + 0.0720 = 1.2377 \text{ inches}$$

$$\Rightarrow \Delta K_I = 52.20 \text{ ksi}\sqrt{in}$$

Use $\frac{1}{2}(49.84 + 52.20) = 51.02 \text{ ksi}\sqrt{in}$

$$\Rightarrow \frac{da}{dN} = 2.160 \times 10^{-4} \text{ in/cycle}$$

For 500 cycles, 0.1080 inches growth

$$\Rightarrow \Delta K_I = 50.77 \text{ ksi}\sqrt{in} \Rightarrow 0.0723 \text{ inches plastic zone size}$$

$$\Rightarrow a_{eff} = 1.0625 + 0.1080 + 0.0723 = 1.2428 \text{ inches}$$

$$\Rightarrow \Delta K_I = 52.31 \text{ ksi}\sqrt{in}$$

Use $\frac{1}{2}(49.84 + 52.31) = 51.08 \text{ ksi}\sqrt{in}$

$$\Rightarrow \frac{da}{dN} = 2.164 \times 10^{-4} \text{ in/cycle}$$

For 500 cycles, 0.1082 inches growth

$$\Rightarrow \Delta K_I = 50.77 \text{ ksi}\sqrt{in} \Rightarrow 0.0723 \text{ inches plastic zone size}$$

$$\Rightarrow a_{eff} = 1.0625 + 0.1082 + 0.0723 = 1.2430 \text{ inches}$$

$$\Rightarrow \Delta K_I = 52.32 \text{ ksi}\sqrt{in} \qquad \underline{\underline{\text{o.k.}}}$$

No threat to NB-3000 allowables.
Satisfies IWB-3612.

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TITLE: FLAW EVALUATION DATE: 6/30/98 PAGE: 10

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Upper Shelf Toughness

[7]. Jung, Y.H. and Murty, K.L., "Effect of Temperature and Strain Rate on Upper Shelf Fracture Behavior of A533B Class 1 Pressure Vessel Steel," in Fracture Mechanics: Nineteenth Symposium, ASTM STP 969, T.A. Cruse, Ed., American Society for Testing and Materials, Philadelphia, 1988, pp. 392-401.

For small amounts of crack growth, say 1.0 mm (0.04 inches), the J-R curve has values of 300 kJ/m² (1,700 in-lb/in²) and higher. For crack extension of 2.5 mm (0.1 inch), the value is above 400 kJ/m² (2,300 in-lb/in²), although Fig. 4 from Reference [7] does not extend out to such data. Using a conversion

$$K_{JC} = \sqrt{JE}\ ,$$

an estimated upper shelf toughness of about 225 ksi$\sqrt{in}$ should apply at 1.0 mm, with 260 ksi$\sqrt{in}$ applying at 2.5 mm.

In actual fact, at the upper shelf, with any sizeable stress, only elastic-plastic fracture mechanics applies.

USING THE UPPER SHELF TOUGHNESS LIMIT OF 225 ksi$\sqrt{in}$, THE CRITICAL FLAW SIZE IS

$$\frac{K_{Ia}}{\sqrt{10}} = \sqrt{\pi a_c}\,(.706)A_0$$

$$a_c = \frac{1}{\pi}\left[\frac{K_{Ia}}{\sqrt{10}(.706)A_0}\right]^2$$

$$= \frac{1}{10\pi}\left[\frac{225}{(.706)(37.5)}\right]^2$$

$$= 2.30''$$

THE NUMBER OF CYCLES TO GROW FROM 0.151" TO THIS SIZE IS

$$a_f = \left[(a_0)^{.025} + 4.587E\text{-}6\,N\right]^{40}$$

$$N = \frac{1}{4.587E\text{-}6}\left[(2.30)^{.025} - (.151)^{.025}\right]$$

$$N = 14{,}650 \gg (500 - 33)$$

3.3 PLASTICITY EFFECTS

The EPRI Elastic-plastic Handbook gives the solution for a two-dimensional flat plate model of a nozzle corner flaw as [8]

$$J_{Elastic} = \frac{\pi a}{E} F^2(a/R;\lambda)\sigma^{\infty 2},$$

where a is the flaw depth, E is the elastic modulus, R is the radius of the nozzle, λ is biaxial loading parameter, F is the weight function, and σ^{∞} is the remote stress in the y-direction ($\lambda\sigma^{\infty}$ is the stress in the x-direction), and

$$J_{Plastic} = \alpha\,\sigma_0\,\varepsilon_0\, a\, h_1(a/R, n;\lambda)\left(\sigma^{\infty}/\sigma_L^{\infty}\right)^{n+1},$$

where α, n, σ_0, and ε_0 are parameters in the Ramberg-Osgood fit to the uniaxial stress-strain curve

$$\frac{\varepsilon}{\varepsilon_0} = \frac{\sigma}{\sigma_0} + \alpha\left(\frac{\sigma}{\sigma_0}\right)^n,$$

h_1 is a weight function, and σ_L^{∞} is the limit stress. A lower-bound solution for the limit stress is given by

$$\sigma_L^{\infty} = \frac{(2b \mp 2R - a)}{4b}\sigma_0\sqrt{3}\ .$$

The elastic portion is known to be

$$J_{Elastic} = \frac{(K_I)^2}{E},$$

where

$$K_I = \sqrt{\pi a}\,(0.706)\,\sigma^{\infty}$$

so, for a = 1.7 inches,

$$K_I = 61.2\ \text{ksi}\sqrt{\text{in}}\ ,\quad J_{elastic} \cong 125\ \text{in-lb/in}^2$$

The plastic portion can be reformulated, according to Ainsworth, to eliminate the error in the Ramberg-Osgood fit by introducing the reference stress

$$\sigma_{ref} = \left(\frac{P}{P_0}\right)\sigma_0,$$

and choosing σ_0 to be the yield stress, 43.5 ksi. The Ainsworth definition will be modified to

$$\sigma_{ref} = \left(\frac{\sigma^{\infty}}{\sigma_L^{\infty}}\right)\sigma_0\ .$$

Then

$$\sigma_{Plastic} = \sigma_{ref}\, b\, h_1 \left(\varepsilon_{ref} - \frac{\sigma_{ref}}{E}\right),$$

where ε_{ref} is the point on the uniaxial stress-strain curve corresponding to σ_{ref}.

The lower-bound limit load is

$$\sigma_L^{\infty} = \frac{(2b-2R-a)}{4b}\,\sigma_0\sqrt{3}.$$

We will choose $2R = 1.625$ inches, the outside diameter of the penetration, and will choose $2b$ to be the width of the plate that simulates the penetration and upper head thickness. The head thickness is 3.875 inches, so that

$$2b = 2\,(3.875\ in.) + 1.625\ in. = 9.375\ in.$$

Then

$$\sigma_L^{\infty} = \frac{(9.375-1.625-a)}{2\,(9.375)}\,(43.5\ ksi)\sqrt{3}.$$

For $a = 1.0625$ in., $\sigma_L^{\infty} = \dfrac{6.6875\ (\text{remaining ligament})}{2\,(9.375)}\,(43.5)\sqrt{3}$

$$= 26.87\ ksi.$$

Note that the hoop stress from internal pressure in the spherical head, 15.625 ksi, is less than this limit load; however, after multiplying by the SCFs, the limit load is less than the very conservative 87.5 ksi.

For purposes of this calculation, a remote tensile stress on the plate equal to 15.625 ksi is chosen. Then

$$\sigma_{ref} = \left(\frac{15.625}{26.87}\right)(43.5) = 25.3\ ksi.$$

At this point ε_{ref} is elastic, so that $\sigma_{Plastic}$ is zero.

If the remote stress is selected as $2 \times 15.625 = 31.25$ ksi, the result is more interesting. Then $\sigma_{ref} = 50.59$ ksi and ε_{ref} is in the inelastic range. For a 5% hardening slope, $\varepsilon_{ref} = 0.00195$ (elastic) + 0.00473 (plastic) = 0.00618.

Then

$$J_{Plastic} = \sigma_{ref}\, b\, h_1 \left(\varepsilon_{ref} - \frac{\sigma_{ref}}{E}\right)$$

$$= (50.53 \text{ ksi})\left(\frac{9.375 \text{ in.}}{2}\right)(1.5)\left(0.00618 - \frac{50.53}{30\times10^3}\right)$$

$$= 1{,}598 \text{ in-lb/in}^2$$

The selection of h_1 is a guess, since the nearest value in the EPRI Handbook is 1.48. Adding,

$$J_{Total} = 125 + 1598 = 1{,}723 \text{ in-lb/in}^2.$$

Note two items. The calculated J is very sensitive to the net section stress if yielding takes place, and assuming that the circumferential stress is uniform across the plate, at the stress concentration value, shows that ductile tearing is very nearly unstable. Secondly, the value of the plastic contribution to J is many times higher than the elastic portion, indicating that the LEFM approach is not valid for these stress levels.

In reality, the lower-bound limit load solution is probably too much of a lower bound; the stress distribution (uniform hoop) is too conservative; and the initial flaw size (1.0625") is too large to make a case that the initial flaw will grow unstably. If the circumferential stress decreases as it should away from the stress concentration, one or two other terms in the nozzle expression could be used to estimate the J-integral.

$$K_I = \sqrt{\pi a}\left[0.706\, A_0 + 0.537\left(\frac{2a}{\pi}\right) A_1\right]$$

For example, $A_0 = 37.5$ ksi and $A_1 = -7.5$ ksi would drop the hoop stress from 37.5 ksi at the inside surface to 30 ksi at 1 inch and to 22.5 ksi at 2 inches. Then, for $a = 1.0625$ inches

$$K_I = \sqrt{\pi(1.0625)}\left[0.706(37.5) + 0.537\left(\frac{2\times1.0625}{\pi}\right)(-7.5)\right] = 43.4 \text{ ksi}\sqrt{\text{in}}$$

This will not help much.

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TITLE: FLAW EVALUATION DATE: 9/9/98 PAGE: 15
SUBJECT: 4.0 CONCLUSIONS BY: K.S. CK: MG SHT: 1 OF 2

BY MAKING THE VERY CONSERVATIVE ASSUMPTION THAT A FLAW EXISTED IN THE CLADDING IN THE WORST ORIENTATION THE DAY THE NEW INSTRUMENT NOZZLE WAS PLACED IN SERVICE, IT WAS FOUND THAT THE POSTULATED FLAW WOULD HAVE GROWN APPROXIMATELY 0.001" DUE TO START-UP/SHUT-DOWN CYCLES. IT WAS ALSO FOUND THAT AN ADDITIONAL 14,650 START-UP/SHUT-DOWN CYCLES ARE REQUIRED TO GROW THIS FLAW TO A CRITICAL SIZE USING A SECTION XI, APPENDIX A APPROACH. AS THE (POSTULATED) FLAW GROWS, THE METHODOLOGY USED IN APPENDIX A BECOMES INCREASINGLY CONSERVATIVE DUE TO THE LARGE PLASTIC ZONE IN THE VACINITY OF THE FLAW. IN THIS REGION, AN ELASTIC-PLASTIC EVALUATION IS MORE

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... APPROPRIATE. HOWEVER, AN EVALUATION OF THIS TYPE IS NOT WARRENTED TODAY SINCE THE (POSTULATED) FLAW IS SUFFICIENTLY SMALL FOR THE REMAINDER OF THE PLANT LIFE.

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CALCULATION SHEET

TITLE: FLAW EVALUATION DATE: 9/8/98 PAGE: 17

SUBJECT: 5.0 REFERENCES BY: KS. CK: MG SHT: 1 OF 2

1) BGE MEMORANDUM TO J.H. OSBORNE FROM C.J. LUDLOW & R.O. HARDIES, "EVALUATION OF PRESSURIZER UPPER LEVEL TAP LEAK", AUG 4, 1998.

2) FRAMATOME TECHNOLOGIES CALCULATION, "ALLOWABLE CORNER FLAWS FOR PRZ UPPER HEAD INSTRU NOZZLE", BY D.E. KILLIAN, AUG. 4, 1998.

3) TELECON, BOB HARDIES (BGE), CHRIS LUDLOW (BGE), ANDRE DRAKE (BGE), BOB NICKELL, PHILIP HASROUNI (HOPPER) AND KEN SAUNDERS (HOPPER), 8/27/98

4) FAX FROM ANDRE DRAKE (BGE) TO PHILIP HASROUNI (HOPPER), 8/10/98

5) ASME BOILER & PRESSURE VESSEL CODE, SECTION XI, 1983.

6) YANKEE PRESSURIZER CLADDING EVALUATION, WESTINGHOUSE ELECTRIC CORP., ATOMIC POWER DIVISION, REPORT # WCAP-2859, 11/2/65.

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CALCULATION SHEET

TITLE: FLAW EVALUATION DATE: 9/8/98 PAGE: 18

SUBJECT: 5.0 REFERENCES. BY: KS. CK: MG SHT: 2 OF 2

7) JUNG, J.H., & MURTY, K.L., "EFFECTS OF TEMPERATURE AND STRAIN RATE ON UPPER SHELF FRACTURE BEHAVIOR OF A533B CLASS 1 PRESSURE VESSEL STEEL", FRACTURE MECHANICS: 19th SYMPOSIUM, ASTM STP 969, T.A. CRUSE, EDITOR, AMERICAN SOCIETY FOR TESTING AND MATERIALS, PHILADELPHIA, 1988.

8) AN ENGINEERING APPROACH FOR ELASTIC-PLASTIC FRACTURE ANALYSIS, EPRI NP-1931, ELECTRIC POWER RESEARCH INSTITUTE, JULY 1981.

FRAMATOME TECHNOLOGIES

CALCULATION 32-5002088-00

"ALLOWABLE CORNER FLAWS

FOR PRZ UPPER HEAD

INSTRU NOZZLE", 8/4/98

20697-3 (12/95)

FRAMATOME TECHNOLOGIES

CALCULATION SUMMARY SHEET (CSS)

Document Identifier 32 - 5002088 - 00

Title ALLOWABLE CORNER FLAWS FOR PRZ UPPER HEAD INSTRU NOZZLE

PREPARED BY:		REVIEWED BY:	
NAME D.E. KILLIAN		NAME P.F. WILLIAMS	
SIGNATURE [signature]		SIGNATURE P.F. Williams	
TITLE PRIN. ENGR.	DATE 8/4/98	TITLE SUPV. ENGR.	DATE 8/4/98
COST CENTER 41020	REF. PAGE(S) 13	TM STATEMENT: REVIEWER INDEPENDENCE	

PURPOSE AND SUMMARY OF RESULTS:

Fracture mechanics analysis is utilized to evaluate a potential corner flaw in a pressurizer upper head instrumentation nozzle at BG&E's Calvert Cliffs Unit 2. Flaw evaluations are performed according to the rules of the ASME Boiler and Pressure Vessel Code, Section XI, Paragraph IWB-3612.

Various initial flaw depths are considered, ranging from 15/16" to 1.71". The largest flaw that could be accepted is about 1.5", since it would permit over 700 heatup/cooldown cycles to 2500 psi pressure. Additional results are presented in Section 8.0.

THE FOLLOWING COMPUTER CODES HAVE BEEN USED IN THIS DOCUMENT:

CODE/VERSION/REV	CODE/VERSION/REV

THE DOCUMENT CONTAINS ASSUMPTIONS THAT MUST BE VERIFIED PRIOR TO USE ON SAFETY-RELATED WORK

☐ YES ☒ NO

RECORD OF REVISIONS

Revision	Pages	Description of Revision	Date
0	All	Original Release	8/98

CONTENTS

1.0 Introduction

Following the discovery of a leaking pressurizer upper head instrumentation nozzle at Baltimore Gas and Electric (BG&E) Company's Calvert Cliffs Unit 2, a long term repair design [1] was formulated for implementation prior to plant restart. It was determined that the pressure boundary has been violated by a crack extending through the Alloy 600 partial penetration attachment weld between the Alloy 690 instrumentation nozzle and the carbon steel upper head. The repair design consists of severing the nozzle above the weld and forming a new pressure boundary by welding the outboard portion of the nozzle to a newly created weld pad on the outside surface of the head. Since this design left the inboard portion of the original nozzle in place and UT was not performed to characterize the extent of the flaw, it has been conjectured that the flaw may extend through the weld and into it the carbon steel head material. To address such a scenario, a typical nozzle inside corner flaw is postulated to be present in the head material and fracture mechanics analysis is performed to evaluate the consequences of flaw growth under cyclic loading conditions. The objective of the flaw evaluation is to determine the number of allowable heatup/cooldown cycles for several initial sizes of postulated nozzle corner flaws extending through the weld, butter, and into the base metal. Failure is associated with unstable crack initiation, as indicated (with safety margin) by the lower bound fracture toughness reference curve of Section XI of the ASME Boiler and Pressure Vessel Code [2].

2.0 Assumptions

Listed below are assumptions that are pertinent to the present fracture mechanics evaluation.

1. It is assumed that the effects of irradiation at the upper head location are negligible and that a value of 40 °F may be used to approximate the RT_{NDT} of the upper head material [7].

2. Thermal stresses may be ignored since the upper head instrumentation nozzle is located a steam dome which, due to low film coefficients, minimizes heat flux into and out of the head under cyclic thermal loading conditions.

3. Attached piping loads at the location of the weld pad on the outer surface of the head are assumed to have a negligible effect on stresses in the corner region.

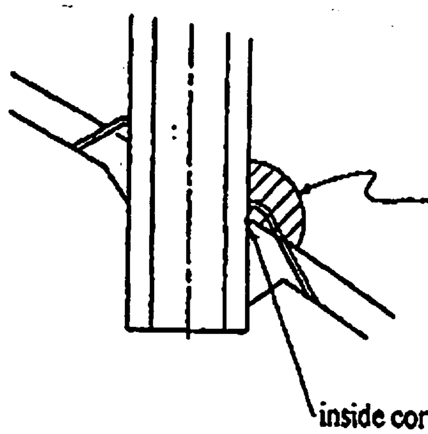
4. Weld residual stresses and cladding effects are assumed to be secondary in nature and need not be included in the present analysis.

5. Faulted condition pressure loads are assumed to be bounded by the 2500 psi design load used for evaluation of normal and upset condition loads.

6. It assumed that increases in pressurizer pressure are limited by operational pressure/temperature (PT) limits and that the highest pressure load is bounding.

3.0 Postulated Flaw Shape

A quarter-circular shape is used to represent a flaw at the inside corner of the upper head penetration for the instrumentation nozzle. The postulated nozzle corner flaw is located in a radial plane relative to the nozzle, starting at an extension of the inside surface of the base metal and including the area of the J-groove, as depicted below.



The flaw is oriented in the radial plane since the dominant stress in the head is the pressure hoop stress around the penetration. Per paragraph 4:7 of the design specification [1], the minimum assumed flaw depth shall be 1-1/16 inches, the height of the J-grooved weld prep. Although this dimension is used in the fracture mechanics analysis for an initial flaw depth, results are also reported for an initial depth of 15/16 of an inch, which is the distance to the inside surface of the weld butter. It is noted that flaw evaluations are performed for a base metal corner flaw by considering that portion of the flaw included in the cladding, weld, and butter areas as being part of the complete flaw.

4.0 Stress Intensity Factor Solution

Although detailed finite element analysis could be utilized to obtain accurate stress distributions through the nozzle, head, and weld regions, and to derive stress intensity factors for specific flaw shapes, the present flaw evaluation relies on available stress intensity factor solutions from the literature using fundamental descriptions of stress.

4.1 Nozzle Corner Flaw

For an arbitrary stress distribution through the vessel head, described by the third-order polynomial,

$$\sigma = A_0 + A_1 x + A_2 x^2 + A_3 x^3, \qquad \text{[Ref. 3, eqn. (G-2.1)]}$$

where x is measured from the inside corner, the stress intensity factor solution for a simulated three-dimensional nozzle corner flaw is:

$$K_I = \sqrt{\pi a}\left[0.706A_0 + 0.537\left(\frac{2a}{\pi}\right)A_1 + 0.448\left(\frac{a^2}{2}\right)A_2 + 0.393\left(\frac{4a^3}{3\pi}\right)A_3\right],$$

[Ref. 3, eqn. (G-2.2)]

where "a" is the radius of the circular-shaped crack front.

4.2 Irwin Plasticity Correction

The Irwin plasticity correction utilized in linear elastic fracture mechanics to account for a moderate amount of yielding at the crack tip is defined for plane strain conditions by

$$r_y = \frac{1}{6\pi}\left(\frac{K_I(a)}{\sigma_y}\right)^2$$

where:

$K_I(a)$ = stress intensity factor based on the actual crack length, a,
σ_y = yield strength.

A stress intensity factor, $K_I(a_e)$, is then calculated based on an effective crack length,

$$a_e = a + r_y.$$

5.0 Applied Stresses

For pressure loading, a membrane hoop stress is calculated for the spherical head at the nozzle penetration as follows:

$$\sigma_h = SCF_{hole} \times SCF_{hillside} \times \frac{PR_i}{2t}$$

where:

Design pressure, $P = 2500$ psi [1]
Radius to the upper head base metal, $R_i = 48.4375$ in. [4]
Upper head thickness, $t = 3.875$ in. [4]

and

Stress concentration for the hole,

$$SCF_{hole} = 2.0 \ [5]$$

Stress concentration for the effect of an oblique, or hillside, nozzle penetration,

$$SCF_{hillside} = 1.2 \ [6]$$

Then the hoop stress used for the evaluation of the radial corner flaw is

$$\sigma_h = 37.5 \text{ ksi},$$

and the polynomial stress coefficients used for the stress intensity factor solution are

$$A_0 = 37.5 \text{ ksi}$$
$$A_1 = 0$$
$$A_2 = 0$$
$$A_3 = 0$$

6.0 Material Properties

Material properties are developed for the SA-533 Grade B, Class 1, upper head low alloy steel plate material [4] for an operating temperature of 653 °F [1].

6.1 Fracture Toughness and Yield Strength

The upper head material is included in the list of ferritic steels for which the lower bound fracture toughness curves in Fig. A-4200-1 of Section XI [2] may be used. At a temperature of 653 °F and for a RT_{NDT} of 40 °F [7], the K_{Ia} and K_{Ic} curves are both well above the upper shelf value of 200 ksi$\sqrt{in}$.

A yield strength of 43.5 ksi at 650 °F is obtained from Section III of the ASME Code [8]. This value is used in the Irwin plasticity correction to calculate an effective flaw depth.

6.2 Fatigue Crack Growth

Flaw growth due to cyclic loading is calculated for the upper head material using the fatigue crack growth rate model from Article A-4000 of Section XI [2].

$$\frac{da}{dN} = C(\Delta K_I)^m \; ,$$

where ΔK_I is the range of applied stress intensity factor in terms of ksi$\sqrt{in}$, da/dN is in terms of inches/cycle. Considering cyclic pressure loads during plant heatup and cooldown conditions, such that the minimum stress intensity factor $K_{min} = 0$, the constants C and m are determined from Fig. A-4300-1 of Section XI [2] using:

$$R = K_{min}/K_{max} = 0$$

For $\Delta K_I > 19$ ksi$\sqrt{in}$,

$$C = 1.01\times10\text{-}7$$
$$m = 1.95$$

For the stress intensity factor solution presented in section 4.1, this flaw growth model can be integrated to express the final flaw depth, a_f, in terms of an initial flaw depth, a_i, for a specified number of fatigue cycles, N, as follows:

$$a_f = \left[a_i^{(1-m/2)} + (1-m/2)C(0.706A_0\sqrt{\pi})^m N\right]^{1/(1-m/2)}$$

7.0 Flaw Acceptance Criteria

For a nozzle corner flaw, the postulated initial flaw size is considered to be acceptable for a given amount of crack growth if the applied stress intensity factor satisfies the fracture toughness requirement of the ASME Code, Section XI, Paragraph IWB-3612 [2] for normal and upset conditions,

$$K_I(a_f) < \frac{K_{Ia}}{\sqrt{10}}$$

where, $K_I(a_f)$ = applied stress intensity factor at the final flaw depth,

K_{Ia} = crack arrest fracture toughness.

8.0 Results

Results of the flaw growth evaluation of postulated nozzle corner flaws are presented below in the form of table of allowable cycles for several values of initial flaw size, ranging from 15/16 inch to 1.71 inches.

Allowable Number of Heatup/Cooldown Cycles vs. Initial Flaw Size for Nozzle Corner Flaws				
a_i (in.)	$K_I(a_i)$ (ksi$\sqrt{in}$)	N (cycles)	a_f (in.)	$K_I(a_{ef})$ (ksi$\sqrt{in}$)
0.9375	45.44	3299	1.7112	63.25
1.0625	48.37	2616	1.7109	63.25
1.5000	57.47	726	1.7110	63.25
1.7106	61.37	1	1.7109	63.25

9.0 Conclusions

Based on 500 heatup/cooldown cycles for a 40-year design life [1], a nozzle corner flaw could have an initial depth into the upper head base metal of 1.5 inches, measured from the "extended" base metal corner, as depicted in the figure on page 6. Such a flaw would grow to about 1.71 inches in 726 cycles to 2500 psi pressure. Above 1.5 inches, only a few cycles would be permitted, until the flaw would reach critical size at 1.71 inches.

10.0 References

1. FTI Doc. No. 08-5002022-00, "Design Specification for Repair/Replacement of Pressurizer Upper Head Instrumentation Nozzles," August 1998.

2. ASME Boiler and Pressure Vessel Code, Section XI, Rules for Inservice Inspection of Nuclear Power Plant Components, 1983 Edition with Summer 1983 Addenda.

3. Marston, T.U., "Flaw Evaluation Procedures – Background and Application of ASME Section XI, Appendix A," EPRI Report NP-719-SR, August 1978.

4. FTI Dwg. No. 02-5002019B-0, "Specification Drawing - Upper Head Instrumentation Nozzle Repair/Replacement"

5. Roark, R.J., Formulas for Stress and Strain, Fourth Edition, McGraw-Hill Book Company, New York, 1965.

6. FTI Doc. No. 32-1176542-02, "Hillside Nozzle Stress Comparison"

7. FTI Topical Report BAW-10046A, Rev. 2, "Methods of Compliance with Fracture Toughness and Operational Requirements of 10 CFR 50, Appendix G," June 1986.

8. ASME Boiler and Pressure Vessel Code, Section III, Rules for Construction of Nuclear Power Plant Components, Division 1 - Appendices, 1986 Edition.