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STRUCTURAL EVALUATION OF COMMONWEALTH EDISON BWR REACTOR PRESSURE VESSEL HEAD STUD CRACKING

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1.0 BACKGROUND

During routine in-service inspection (ISI) in January 1989, at the Dresden 2 plant, suspected crack indications were observed by UT in two RPV head studs below the first engaged thread in the RPV flange. The indications were detected with a Section XI Code UT technique where a straight beam (0° L wave) introduced from the upper end traverses the length of the stud. The calibration reflector in this case was a 3/8 inch diameter flat bottomed hole. The reflections received from the indications had amplitudes of 10% to 25% of the calibration reflector, and therefore were not required by Code to be recorded. However, these indications were evaluated as crack indications. For this reason, studs with indications were removed for additional testing. Two available spare studs were installed in place of the two with indications. All other studs (90 out of a total of 92) were UT inspected without observation of other crack indications.

Magnetic particle examination was performed on both studs to verify the presence of cracks. One stud showed cracks in seven threads extending to 40% of the circumference. The other stud contained cracks in two threads extending 50% of the circumference. Depth measurements were made with a 70° shear wave probe from the center bore hole. Maximum depths were estimated to be 0.88 inches in one stud and 2.09 inches in the other. Subsequently, a metallurgical examination was performed on the stud with the lesser crack depth and it was determined that the actual depth was 0.7 inches. The metallurgical evaluation reported the cause of the cracking to be stress corrosion cracking (SCC) initiating at pits. Fig 1-1 and 1-2 show the approximate shapes of the cracks based on UT, and the remaining cross sections.

The studs in which crack indications were observed were fabricated from material specified as SA-193 Class 3, with Code Case 1335-1. The outside diameter of the stud is approximately 6 in. and the inside diameter is one inch. During refueling, the stud, including the lower portion which is threaded, is exposed to the stagnant air saturated water environment following vessel flooding. The stud threads near the vessel flange surface may remain wet until plant startup, when the flange reaches a temperature high enough to evaporate the water.

The metallurgical examination of the stud with the 0.7 inch deep crack showed mixed mode transgranular cracking and branching, which, in this alloy, is typical of stress corrosion cracking. The probable cause of the SCC is the exposure of the studs in the preloaded condition to oxygenated water during outages since extensive oxidation and pitting was observed on the studs. The metallurgical evaluation indicates that cracking originated on the outer edge of the stud at pitted locations near the thread roots. The cracked studs had been in service for about 18 years, including 11 refueling outages.

The specification for the studs sets a minimum tensile strength of 145 ksi. At the time of design, no specifications on maximum stud tensile or yield strength were defined. USNRC Regulatory Guide 1.65, issued in October 1973, requires a maximum tensile strength of 170 ksi on RPV studs, because stud material with greater than 170 ksi (with the corresponding hardness of R_C 38) had shown susceptibility to SCC. The metallurgical evaluation reports a tensile strength value of 180 ksi and a hardness of R_C 38 for the material in the outside threaded area of one cracked stud. The metallurgical evaluation also reports a Charpy V-notch (CVN) impact toughness of 21 ft-lbs at +10°F, while the CMTR for that heat had reported 36 to 52 ft-lbs at +10°F. The reasons for the drop in toughness are being investigated. However, for the purpose of the structural evaluation described here, the current measured CVN values are used.

This report describes the structural evaluation of the remaining vessel studs at Dresden-2 assuming postulated cracks in the remaining ninety original studs. Also included is the structural evaluation in terms of allowable number of cracked studs for the other CECO BWR units.

Since these studs were examined in 1989 and found to be free of indications, it is conservative to postulate crack depths equal to the threshold of detection for the UT procedure used. With this initial flaw size assumption and a crack growth rate based on the maximum depth, the crack depth at the end of the current outage is estimated. Fracture toughness values are determined based on the current measured CVN data. Fracture margins are determined for the limiting condition for the stud-bolt up. Finally, the minimum stud area required to meet the ASME code stress limits is determined. This will provide information on the number of cracked studs that can be tolerated while still maintaining the ASME code requirements for joint integrity.

Figure 1-1
Cracked Cross Section of Stud # 61-198-47

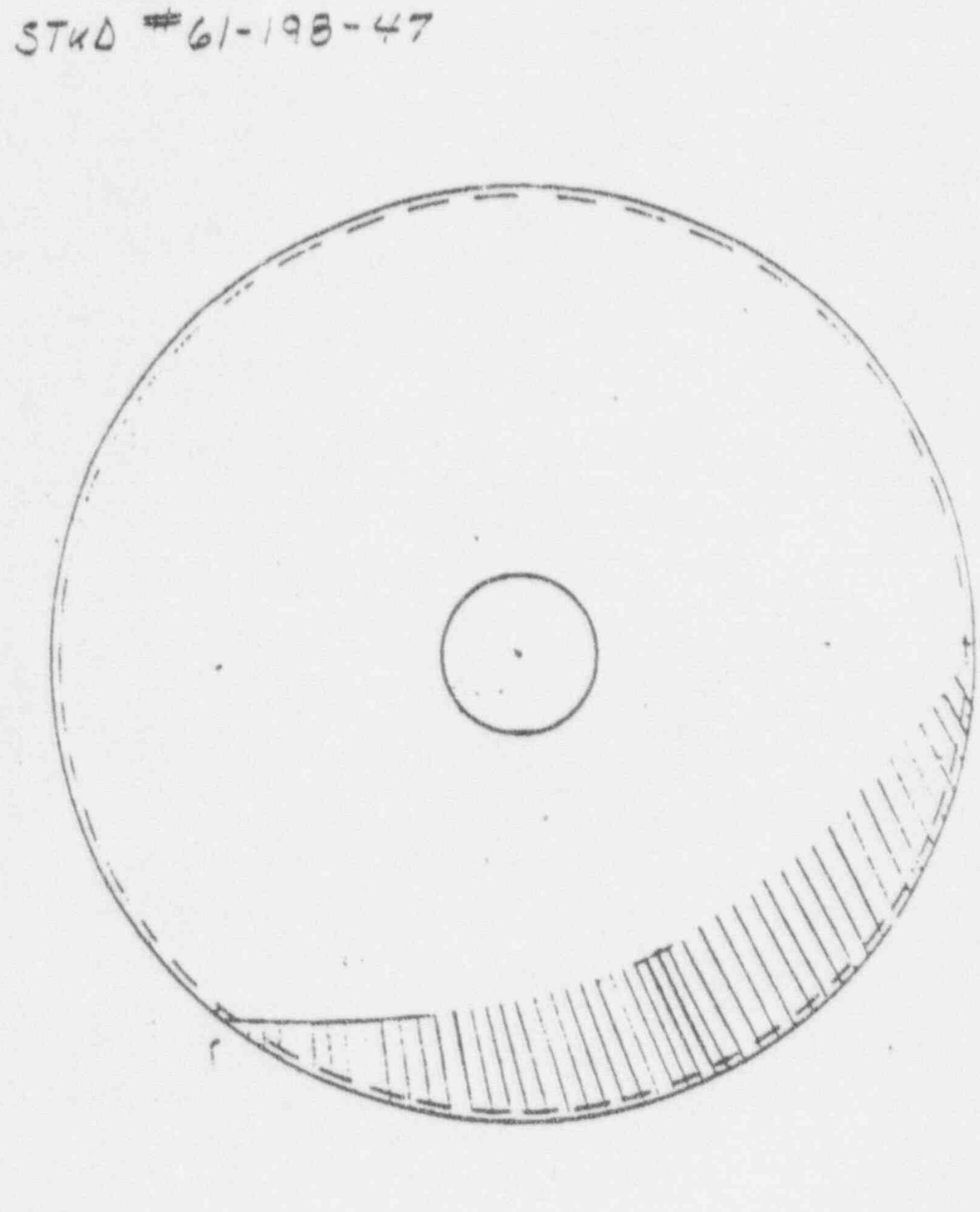
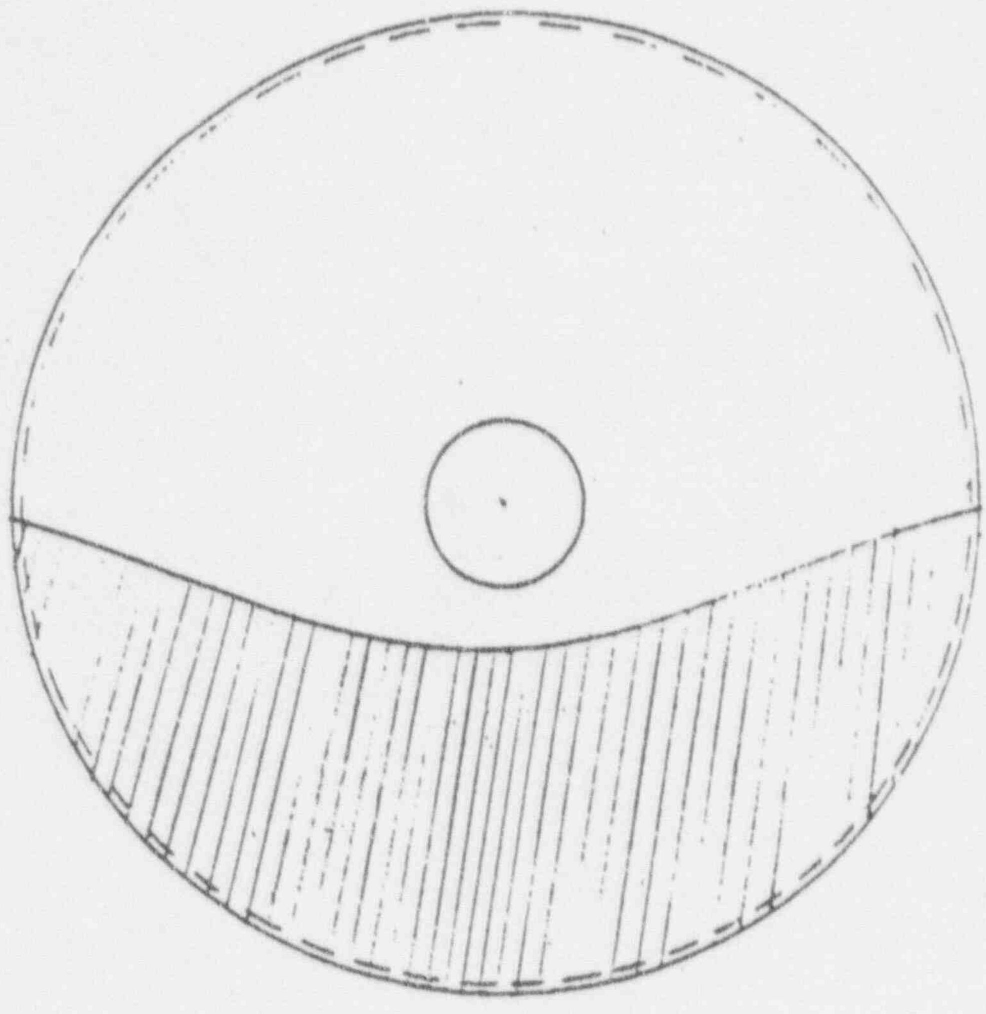


Figure 1-2
Cracked Cross Section of Stud # 61-198-70

STUD # 61-198-70



2.0 CRACK GROWTH ASSESSMENT

The vessel studs are not exposed to the water environment when the pressure vessel is at temperature since the studs are dry. The only time when the studs are in tension and exposed to the water environment is after refueling and retensioning when the vessel head is in place but the flange temperature is not high enough to allow evaporation of the water. This time is estimated to be on the average 3 weeks for each refueling outage. During this period the lower portion of the stud is exposed to stagnant air saturated water, but at temperature below 212°F. Stress corrosion cracking can occur under these conditions since the applied stud loads are high and the environment is stagnant. The Dresden 2 head has experienced 11 bolt up cycles when the cracked studs were found in 1989. It is assumed that crack initiation occurs in half this time i.e., 6 cycles. The total period for crack growth is (6 bolt up cycles) (3 weeks/bolt up) (7x24 hours/week) = 3024 hours. Assuming 2 in. growth in this time the average crack growth rate is 6.6×10^{-4} in/hour. This is within the range of predictions (Appendix A) from analytical models for low alloy steel at high temperatures. The estimated crack growth rate of 6.6×10^{-4}/hr. is higher than the bounding low sulfur line but is below the model predictions for the high sulfur line for stagnant conditions. The effect of the lower temperature of the studs and the moderate sulfur content could explain why the crack growth rate is between the low sulfur and the high sulfur predictions. Nevertheless, the comparison with the model predictions does support the premise that the exposure to the stagnant water environment during refueling could have caused observed cracking. If the crack growth rate stayed at this level for the 1989 and 1991 bolt up cycles the increment in crack depth is $2 \times (3 \times 7 \times 24) \times 6.6 \times 10^{-04} = 0.6$ in. with the assumed initial flaw size of 0.7 in. corresponding to the threshold for UT inspection. The estimated crack depth following the current bolt up is 1.3 in. This crack depth value will be used in the fracture margin assessment.

3.0 FRACTURE TOUGHNESS ASSESSMENT

3.1 CVN Energy

The fracture toughness measured from one heat of the stud material ranged from 36 to 52 ft-lb at 10°F (Table 3-1). However, Table 3-1 shows that the measured CVN energies of specimens taken from one of the cracked studs are considerably lower. It is not clear whether this variation in CVN values is due to heat to heat variations or an aging phenomenon. Nevertheless, for this evaluation the CVN values based on the specimens removed from the cracked stud will be used conservatively. Table 3-2 shows the hardness and tensile data.

The most limiting condition for the vessel stud from the fracture mechanics viewpoint is the bolt up condition. The temperature for bolt up can be as low as 80°F and the loading is essentially the maximum value corresponding to stud tensioning. Other conditions such as hydrotest and normal operation are not as severe as bolt up since the temperature (and therefore, the toughness) is significantly higher. Furthermore, the applied load on the stud during tensioning is higher than that under the hydrotest and operating conditions. Thus, the vessel bolt up represents a 'proof test' and after a successful bolt up, the likelihood of a fracture problem in the operating condition is negligible. For the purpose of this fracture assessment an average CVN value of 27.5 ft-lb corresponding to the mid-wall location and 80°F temperature will be used.

3.2 Fracture Toughness Calculation

Fracture mechanics assessments require the conversion of the CVN values to K_{IC} values. Several empirical relationships (Table 3-3) are available relating K_{IC} to CVN. These fall into two broad areas - transition temperature range and upper shelf range. Table 3-4 shows the K_{IC}-CVN relationships and the calculated fracture toughness values. The transition range predictions probably represent lower bound values and the upper shelf correlation may be an upper bound. Amongst the transition range relationships the Barsom-Rolfe correlation predicts K_{Id} values and may be overly conservative for fracture margin calculations since the bolt loading is virtually static. The Corten-Sailors correlation is more widely used and probably represents the best transition range prediction. Thus for the purpose of the fracture assessment it is concluded the fracture toughness of the stud material is in the range of 81.3 ksi$\sqrt{in}$ (Corten-Sailors transition range) to 123.7 ksi $\sqrt{in}$ (Rolfe-Novak-Barsom upper shelf) at the 80°F bolt up condition.

3.3 References

3-1 J.M. Barsom and S.T. Rolfe, "Correlations Between K_{Ic} and Charpy V-Notch Test Results in the Transition-Temperature Range," Impact Testing of Metals, ASTM STP 466, American Society for Testing and Materials, Philadelphia, 1970, pp. 281-302.

3-2 J.T. Corten and R.H. Sailors, "Relationship Between Material Fracture Toughness Using Fracture Mechanics and Transition Temperature Tests," T. & A.M. Report No. 346, University of Illinois, Urbana, Aug. 1971.

3-3 R. Roberts and C. Newton, "Report on Small-Scale Test Correlations with K_{Ic} Data," WRC Bulletin 299, Nov. 1984.

3-4 S.T. Rolfe and S.R. Novak, "Slow-Bend K_{Ic} Testing of Medium-Strength High-Toughness Steels," Review of Developments in Plane Strain Fracture-Toughness Testing, ASTM STP 463, American SOciety for Testing and Materials, Philadelphia, 1970, pp. 124-159.

TABLE 3-1

Charpy V-Notch Results (ft-lbs)

	10°F	Room Temperature	80°F	150°F
Heat 67-80278 CMTR at 1/2 Radius	47, 52, 36	-	-	-
Stud 61-198-47				
Near OD	22, 18	31, 32	39, 31	47, 47
1/2 Radius	21, 20	22, 25	28, 27	47, 46
Near Bore	20, 20	25, 23	22, 26	44, 46

TABLE 3-2

Hardness and Tensile Test, Results

	Tensile Strength (ksi)	Yield Strength (ksi)	% Elongation	% Reduction in Area	Rockwell C Hardness
SA193 Cl.3, per Code Case 1335-1	145 min	130 min	12 min	40 min	No Require.
Stud 61-198-47					
Near OD	180.2	167.8	17.4	53.5	38/39
1/2 Radius	173.0	155.2	18.0	56.5	34/35
Near Bore	164.0	145.7	17.4	54	32/33
Heat 67-80278 CMTR at 1/2 Radius					
Test 1	156.5	140.0	19.0	59.1	32/36
Test 2	160.0	145.0	18.5	56.9	36/38
Test 3	154.0	137.5	18.5	57.3	31/33

TABLE 3-3

Typical Fracture Toughness-CVN Relationships

Transition Range

(1) Barsom and Rolfe (Reference 3-1)

$$(K_{Id})^2/E = 5\ CVN$$

(KId in psi√in, E in psi and CVN in ft-lb)

(2) Corten & Sailors (Reference 3-2)

$$K_{Ic} = 15.5\ \sqrt{(CVN)}$$

(K_{Ic} in ksi√in, CVN in ft-lb)

(3) Roberts and Newton (Reference 3-3)

$$K_{Ic} = 9.35\ (CVN)^{0.63}$$

(K_{Ic} in ksi √in, CVN in ft-lb)

Upper Shelf

(4) Rolfe-Novak-Barsom (Reference 3-4)

$$(K_{Ic}/Sy)^2 = 5\ (CVN/Sy - 0.05)$$

(K_{Ic} in ksi √in, Sy in ksi, CVN in ft-lb)

TABLE 3-4

Predicted Fracture Toughness as a Function of the Measured CVN

		10	RT	80	150
	Temp (°F) CVN (half radius) average	20.5	23.5	27.5	46.5
(1)	K_{Id} (Barsom & Rolfe) ($E = 30 \times 10^6$ psi) Transition Range	55.4	59.47	64.20	83.50
(2)	K_{Ic} (Corten & Sailors) Transition Range	70.20	75.10	81.30	105.70
(3)	K_{Ic} (Roberts-Newton) Transition Range	62.70	68.30	75.40	105.0
(4)	K_{Ic} (Rolfe-Novak-Barsom) $S_y = 155$ ksi Upper Shelf	-	-	123.7	173.3

4.0 FRACTURE MECHANICS ASSESSMENT

The fracture mechanics assessment was conducted in two steps. First, the applied value of stress intensity factor, K, was calculated as a function of crack depth for the various operating conditions. The fracture margin assessment was then performed considering the measured fracture toughness properties of the closure studs.

4.1 Applied Stress Intensity Factor Calculation

The general expression for calculating the stress intensity factor, K for any cracked geometry is the following:

$$K = \sigma\ (F)\ \sqrt{(\pi a)} \qquad (4\text{-}1)$$

Where, σ is the nominal stress, a is the crack depth and F is the amplification factor. The magnitude of F is a function of the component geometry and crack depth.

Based on the observed crack geometries in the two closure studs removed from service, the crack geometry shown in Figure 4-1 was used in calculating the K values. A literature search was conducted to obtain the value of F for various crack depths. Reference 4-1 gives the experimentally determined values of K for a round bar with the crack geometry shown in Figure 4-1, subjected to three point bending. Based on the experimental values reported in Reference 4-1, a mathematical expression for K was derived in Reference 4-2. The corresponding mathematical expression for the amplification factor, F, is the following:

$$F=(\sqrt{\pi}/8)[3.75-10.93(a/D)+20.05(a/D)^2 -19.93(a/D)^3+7.56(a/D)^4]/(1-a/D)^2 \qquad (4\text{-}2)$$

The closure stud is subjected to tension loading rather than bending. However, due to the constraints at the each end, the closure studs are likely to experience a loading that may be somewhere in between the three point bending and pure tension. Since there are no K solutions available in the literature for an edge cracked hollow stud subjected to tension load, the K values were obtained by multiplying the bending case K values by a ratio, R given as the following:

$$R = K_{p,t}/K_{p,b} \qquad (4\text{-}3)$$

Where, $K_{p,t}$ = Stress intensity factor for a single-edge cracked plate subjected to tension with crack length to plate width ratio the same as the crack depth to stud diameter ratio.

$K_{p,b}$ = Stress intensity factor for a single-edge cracked plate subjected to bending with crack length to plate width ratio the same as the crack depth to stud diameter ratio.

The $K_{p,t}$ and $K_{p,b}$ value were obtained using the mathematical expressions given in Reference 4-3.

The other key information needed to calculate the K value in equation (4-1) is the stress, σ. The stresses in the studs do not change significantly after the initial bolt up. Also, the temperature (correspondingly, the material fracture toughness) is lowest during the bolt up compared to any other plant operating conditions. Therefore, it is reasonable to conclude that the bolt up condition represents the most limiting condition from the fracture mechanics assessment considerations.

Reference 4-4 indicates that a pressure of 6600 psi is applied in the stud tensioner during the bolt up. This is equivalent to a stud tensioning load of 1,442,100 lbs. The corresponding stud nominal stress, σ is 52780 psi. This value of nominal stress was used in calculating the K values.

Thus, the K values were evaluated using the following expression:

$$K = \sigma\ (F)(R)\sqrt{(\pi a)} \tag{4-4}$$

Table 4-1 shows the calculated values of K as a function of crack depth. The last column in Table 4-1 shows the K values for the tension case and the second from last column shows the values for bending case. The actual K value at any crack depth is expected to be somewhere between the bending and tension case values.

4.2 Fracture Margin Assessment

As indicated in Section 2, the estimated crack depth in any cracked stud following the current bolt up is 1.3 inch. An examination of Table 4-1 indicates that the applied value of K at this crack depth ranges from 82.3 ksi√in to 117.2 ksi√in. In Section 3, the fracture toughness of the stud material at the bolt up condition was estimated to range from 81.3 ksi√in to 123.7 ksi√in. Thus, the available material toughness is in the range of applied K values. Thus, even with the conservative assessment, crack extension is not predicted. Furthermore, a stud failure or a pop up type crack extension during stud tensioning would definitely be noticed and recorded. Since a review of the stud tensioning records indicated no such event, it is concluded that the toughness of each of the studs is at least equal to or greater than the applied value of K. Thus, the fact that a stud did not experience failure in a 'proof test' assures that the stud will maintain its structural integrity at least till the next bolt up.

4.3 References

4-1 Bush, A.J., "Experimentally Determined Stress-Intensity Factors for Single-Edge-Crack Round Bars Loaded in Bending," Experimental Mechanics, 16, 249-257 (1976).

4-2 Underwood, J.H. and Woodward, R.L., "Wide Range Stress Intensity Factor Expression for an Edge-Cracked Round Bar Bend Specimen," Experimental Mechanics, 29, 166-168 (1989).

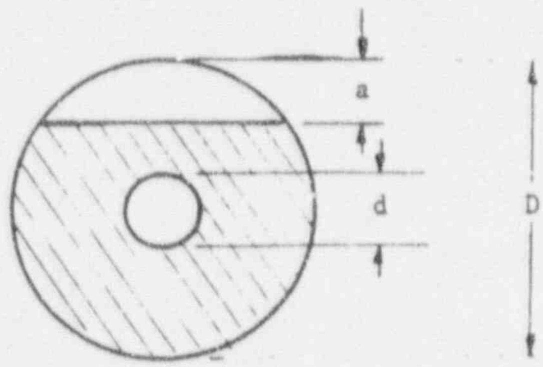
4-3 Tada, H., Paris, P.C. and Irwin, G.R., The Stress Analysis of Crack Handbook, Del Res. Corp., Hellertown, PA. (1973).

4-4 Telephone conversation between Tom Spry of CECO and H. Mehta of GE, January 12, 1991.

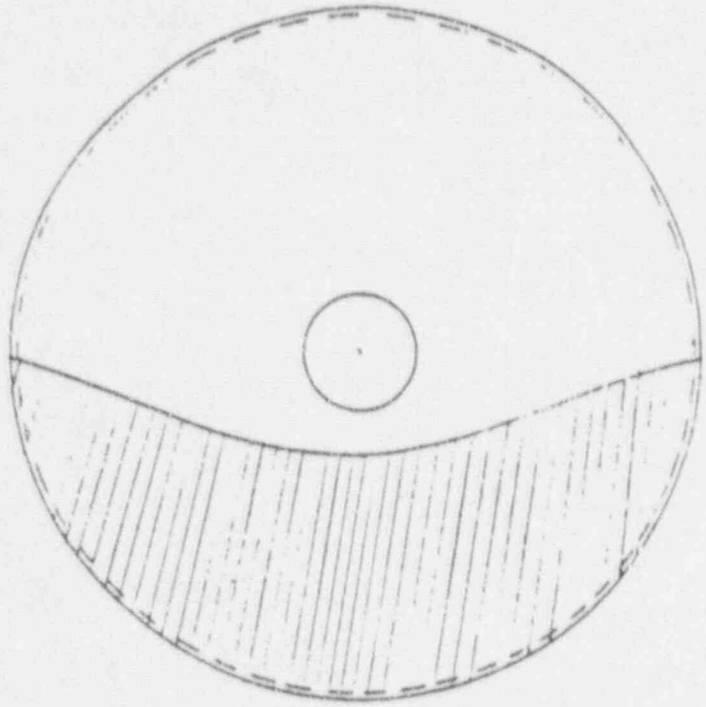
TABLE 4-1

K Calculation for Cracked Studs

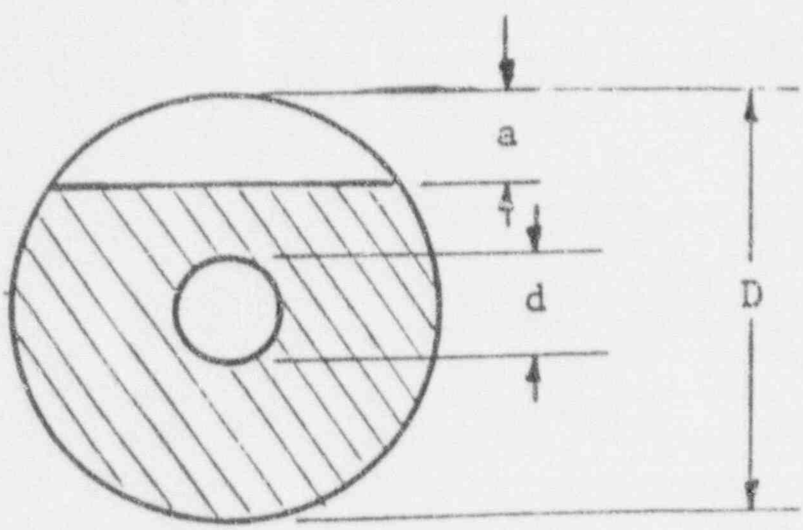
$k = \sigma(F)(R)\sqrt{\pi a}$



a	a/D	F	R	K,t	K,b
0.10	0.017	0.819	1.024	24.8	24.2
0.20	0.033	0.808	1.050	35.5	33.8
0.30	0.050	0.798	1.080	44.1	40.9
0.40	0.067	0.790	1.111	51.9	46.7
0.50	0.083	0.782	1.143	59.2	51.8
0.60	0.100	0.776	1.177	66.2	56.3
0.70	0.117	0.772	1.212	73.2	60.4
0.80	0.133	0.768	1.247	80.2	64.3
0.90	0.150	0.766	1.283	87.2	68.0
1.00	0.167	0.765	1.318	94.4	71.6
1.10	0.183	0.766	1.354	101.8	75.2
1.20	0.200	0.768	1.389	109.3	78.7
1.30	0.217	0.772	1.424	117.2	82.3
1.40	0.233	0.777	1.458	125.4	86.0
1.50	0.250	0.783	1.492	133.9	89.7
1.60	0.267	0.792	1.526	142.9	93.7
1.70	0.283	0.802	1.558	152.4	97.8
1.80	0.300	0.813	1.591	162.4	102.1
1.90	0.317	0.827	1.623	173.1	106.6
2.00	0.333	0.842	1.656	184.5	111.4



(a) Cross Section of Cracked Stud



(b) Assumed Geometry for K calculation

Figure 4-1

5.0 ASME CODE MARGIN ASSESSMENT

The minimum stud area required to maintain ASME Code margins is evaluated in this section. The calculations are based on the criteria presented in the ASME Code Section III Appendix E, "Minimum Cross-Sectional Area".

E-1200 gives the criteria to determine the minimum stud area. The design load is given by;

$$W_{m1} = H + H_p \tag{5-1}$$

$$= 0.785G^2P + (2b \times 3.14GmP)$$

$$W_{m2} = 3.14bGy \tag{5-2}$$

where W_{m1}= minimum required stud load for the Design Pressure
W_{m2}= minimum required stud load for gasket seating
H= total hydrostatic end force
G= Diameter at location of gasket load reaction
P= Design pressure
b= effective gasket or joint contact surface seating width
m= Gasket Factor (from Table E-1210-1)

The minimum required stud area is given by;

$$A_{m1} = W_{m1}/S_b \tag{5-3}$$

$$A_{m2} = W_{m2}/S_a \tag{5-4}$$

where S_a= allowable stud stress at atmospheric temperature
S_b= allowable stud stress at design temperature

Note that the actual stud area must be greater than either A_{m1} or A_{m2}.

Based on the above criteria for minimum stud area, the minimum number of studs is given by the greater of:

$$N_1 = A_{m1}/A \quad \text{or,} \quad N_2 = A_{m2}/a \tag{5-5}$$

where A is the cross-sectional area of one stud.

5.1 Minimum Stud Area Required For Dresden 2 & 3

Based on the criteria given in Section 5.0, the minimum stud area and number of studs can be calculated. For self energizing gasket types, Table E-1210-1 of the ASME Code states that the gasket factor, m, is 0. However, for this calculation, it was conservatively assumed that the gasket factor is 6 which is a high number in Table E-1210-1.

Substituting the appropriate numbers into equations 5-1 through 5-5 the required stud area is 2030.3 in^2. The minimum number of studs is 79. The actual number of studs is 92. Therefore, there is significant margin since the actual number of studs is significantly greater than the minimum number of studs.

5.2 Minimum Stud Area For Quad Cities and La Salle 1 and 2 Plants

The minimum stud area and the number of studs was also calculated for the Quad Cities and La Salle 1 and 2 plants. The calculation of required stud area and number of studs is given in Reference 5-1 and 5-2 for La Salle 1 and La Salle 2 respectively. The results for Quad Cities 1 & 2 are the same as for Dresden 2 based on identical geometries. Results of these calculations are shown in Table 5-1. Also shown in Table 5-1 are the actual number of studs. Since the cracked studs considered here are assumed to carry no loads, conservative design practice requires that the distribution be uniform (i.e., no clustered cracked studs).

5.3 Acceptable Number of Partially Cracked Studs

Another measure by which the structural margin of the reactor head closure can be assessed is the maximum number of partially cracked studs (with the maximum estimated crack depth) that can be present while still maintaining the code required minimum total stud area. It was assumed in this calculation that the remaining area at a cracked stud contributes to the total stud area.

Table 5-2 shows the results of this calculation. The calculation for Dresden-2 is somewhat conservative since a higher stud load used in the vessel design report was considered. The results in Table 5-2 clearly demonstrate that a significant number of cracked studs can be tolerated while still monitoring the required code structural margin.

Experience with the evaluation of preloaded studs shows that the compliance of a stud does not increase significantly for moderate crack depths. Thus preload loss is not significant for the range of crack depths evaluated here. Therefore, there is no specific requirement on the distribution of the partially cracked studs (i.e., they can be adjacent to each other). Furthermore, for the self sealing O-rings the load needed for the sealing action is small.

5.4 References

5-1 "Analytical Report For LaSalle County Station Unit No.1", Report No. CENC-1250, Combustion Engineering, Chattanooga, Tennessee.

5-2 La Salle II Vessel Stress Report, CBIN Contract No. 72-2046, Chicago Bridge and Iron Nuclear Company.

Table 5-1

Required Stud Area and the Number of Studs[1]

	La Salle 1	La Salle 2	Quad Cities 1&2	Dresden 2&3
Required Stud Area(in^2)	1810.0	1787.7	2030.3	2030.3
Required # of Studs[2]	66	73	79	79
Actual # of Studs	68	76	92	92

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1. Assumes uniform distribution i.e., no clustered cracked studs.

2. Assumes that the remaining studs are fully cracked and have no load carrying capability.

Table 5-2

Allowable Maximum Number of Partially Cracked
Studs to Code Required Margin

Plant	# of Studs
Dresden 2 & 3	43
Quad Cities 1 & 2	43
La Salle 1	15
La Salle 2	17

6.0 SUMMARY AND CONCLUSIONS

This report describes the structural evaluation of the remaining vessel studs at Dresden-2 assuming postulated cracks in the remaining ninety original studs. Since these studs were examined 1989 and found to be free of indications, it is conservative to postulate crack depths equal to the threshold of detection (0.7 in.) for the UT procedure used. With this initial flaw size assumption and a crack growth rate based on the maximum depth, the crack depth at the end of the current outage is estimated. Fracture toughness values are determined based on the current measured CVN data. Fracture margins are determined for the limiting condition for the stud-bolt up. Finally, the minimum stud area required to meet the ASME code stress limits is determined. This provides information on the number of cracked studs that can be tolerated while still maintaining the ASME code requirements for joint integrity.

The results of the analysis confirm that crack extension is unlikely to occur for the limiting bolt up condition. This was confirmed by the stud tension experience where no unusual load drop or compliance changes indicative of crack extension was observed. Since the studs have undergone the 'proof test' condition during bolt up, no fracture concerns arise for the pressure test and normal operation conditions.

The analysis of the minimum required stud area for Dresden 2 plant shows that up to 13 could be fully cracked without violating the code stress limits. If partial cracking (i.e., Postulated Crack depth of 1.3 in.) is assumed, cracking of up to 43 bolts can be tolerated. Similar analyses were also performed for other CECO BWRs. The results of these analyses indicate that there is significant structural margin relative to the code requirements even with a large number of cracked studs. The margin to failure is substantially higher and confirms the overall structural margin in the flanged joint.

APPENDIX

Theoretical Crack Propagation Rate/Stress Intensity Factor Relationships for Low Alloy Steels

Appendix

Stress corrosion cracking in ferritic steels is a function of several parameters: applied stress intensity factor, sulfur content, temperature, water conductivity, ECP, flow rate. Several models have been developed to mechanistically address these variables [A-1]. These models are in large part applicable to 288°C water environment. The conditions for SCC in the stud occur at lower temperature <100°C and for a short period of time. The applied stresses are high. And the environment is stagnant. The justification for the applicability of the high temperature models to the stud environment is therefore not clear. Nevertheless it is useful to compare the crack growth rate deduced from the experience with the worst cracked stud with the limiting crack growth rates based on analytical models.

Figure A-1 from Reference A-1 shows the theoretical crack propagation rate/stress intensity relationships for low allow steel in low flow rate water at 550°F. The analytical equations corresponding to the two relationships shown in Figure A-1 are the following:

For high sulfur,

$V_T = 1.23 \times 10^{-5} K^{1.4}$ in/hr.

for low sulfur,

$V_T = 5.32 \times 10^{-12} K^{4}$ in/hr.

where K is in ksi $\sqrt{in}$.

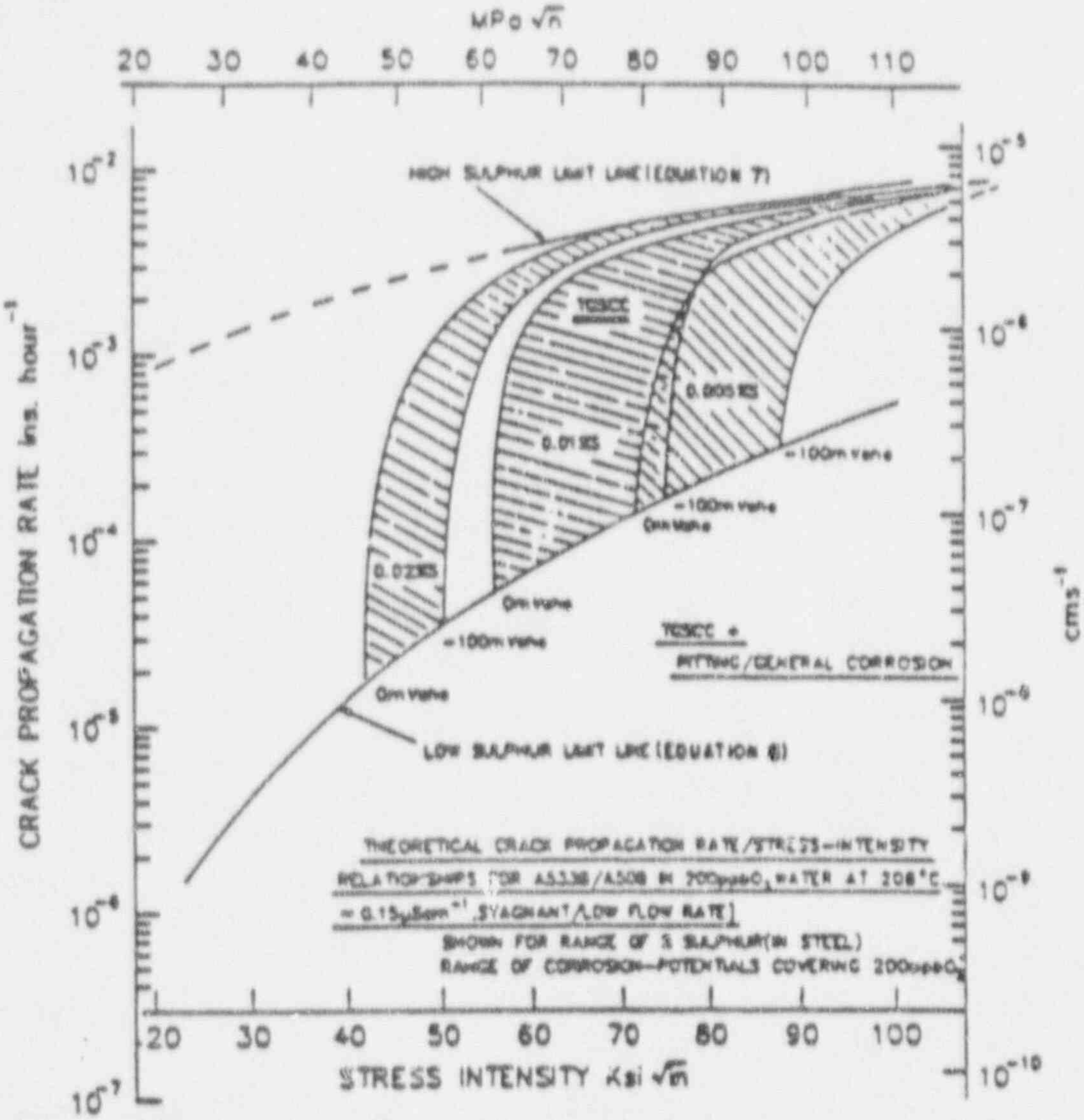
An average value of 40 ksi $\sqrt{in}$ was assumed for the crack growth rate calculation using the above relationships. Accordingly, the predicted crack growth rates are 2.2×10^{-3} in/hr and 1.4×10^{-5} in/hr, respectively.

The estimated crack growth rate of 6.6×10^{-4} in/hr lies in between the two predicted crack growth rates. Note that the analytical model predictions are based on 550°F water, whereas the water environment experienced by the closure studs is at less than 212°F, which is expected to result in lower crack growth rate.

Therefore the crack growth rate of 6.6×10^{-4} in/hr used in the structural evaluation is a reasonable value.

Reference

A-1 Ford, F.P., "Status of Research on Environmentally Assisted Cracking in LWR Pressure Vessel Steel," Trans. of ASME, Journal of Pressure Vessel Technology, 1988.



Theoretical crack propagation rate/stress intensity relationships for low-alloy steel in low flow rate water at 288°C, for various combinations of sulphur content (in steel) and corrosion potential.

Figure A-1