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BRUNSWICK STEAM ELECTRIC PLANT, UNIT 2
FEEDWATER NOZZLE FRACTURE MECHANICS ANALYSIS

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CLASS III

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CONTENTS OF THIS REPORT
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CONTENTS

TABLES

ILLUSTRATIONS

ABSTRACT

The current revision of this report is based on actual feedwater cycling data collected for Brunswick 2 since the original (July 1984) revision of this report. In addition, the triple thermal sleeve design previously analyzed has not been implemented, and there are currently no plans to do such. Therefore, the ring-interference fit, single thermal sleeve design (original design) was evaluated in this report.

This report provides a plant-specific fracture mechanics assessment of the Brunswick 2 feedwater nozzles to show interim compliance with NUREG-0619 and NRC Generic Letter 81-11, dated February 20, 1981. The evaluation was based upon (1) the plant operating history supplied by Carolina Power and Light Company (CP&L), (2) low feedwater flow characteristics determined from actual plant feedwater cycling measurements, and (3) Moss Landing test data. The evaluation considered an initial crack depth of 0.25 inch as specified in NUREG-0619. The results show that stress cycling from actual temperature and flow profiles results in the growth of an initial 0.25-inch crack to greater than 1 inch during the remaining life of the plant. Using the 1989 ASME Section XI fatigue crack growth curves, the analysis shows that the postulated 0.25-in. crack becomes 1 inch deep 32.3 years after the startup date.

Recommendations are made for updating the plant operating history, as well as monitoring leakage flow, and using using those results to re-evaluate the crack growth analysis contained herein.

1. INTRODUCTION

This report provides a plant-specific feedwater nozzle fracture mechanics assessment based on the Brunswick Steam Electric Plant, Unit 2 (hereafter called Brunswick 2) plant operating history and actual feedwater cycling data. This is in response to the Nuclear Regulatory Commission (NRC) requirements regarding feedwater nozzle crack growth. These requirements are contained in the NRC Generic Letter 81-11, which states that a fracture mechanics evaluation must predict an end-of-design-life crack size of 1 inch or less.

1.1 BACKGROUND

The General Electric Company (GE) feedwater nozzle final report (Reference 1) recommended design and operational changes to minimize both the probability of crack initiation and rate of crack growth in feedwater nozzles. The low flow feedwater controller discussed in Reference 1 would not significantly reduce the probability of crack initiation, but would reduce crack growth. The NRC (NUREG-0619) accepted the GE recommendation (Reference 1) and required that operating reactors install a low flow feedwater controller with the characteristics described in Reference 1 and reroute the Reactor Water Cleanup System (RWCS) flow to all of the feedwater lines. The low flow controller required above must meet strict requirements specified in Subsection 3.4.4.3 of Reference 1. The NRC later clarified its position in Generic Letter 81-11, stating that plant-specific analyses may be performed to justify not implementing such modifications.

With respect to low flow feedwater controller installation assessment, feedwater nozzle crack growth rate analysis is required for Brunswick 2.

1.2 OBJECTIVE

Because of the absence of Brunswick 2 low flow feedwater controller data, temperature measurement hardware was installed subsequent to the July 1984 revision of this report. The data collected by that hardware for the last fuel cycle was utilized as a basis for defining the actual Brunswick 2 feedwater cycling characteristics used in the current analysis. The data collected was considered typical for the entire design life of the reactor.

This report provides a plant-specific fracture mechanics assessment of the Brunswick 2 feedwater nozzles to show compliance with the requirements of NUREG-0619 as amended by Generic Letter 81-11, dated February 20, 1981. The purpose of this analysis is to determine whether stress cycling from actual controller temperature and flow fluctuations will result in a final crack depth of 1 inch or less during a 40-year plant design life. The evaluation considers an initial crack depth of 0.25 inch, as specified in NUREG-0619.

1.3 TECHNICAL APPROACH

This analysis evaluates the growth of a 0.25-inch crack over a projected 40-year plant design life.

1.3.1 Feedwater Flow Cycling

The feedwater flow cycling was determined from actual feedwater temperature and flow data obtained from CP&L (Reference 2).

1.3.2 Thermal Cycling

The thermal cycling of the fluid at the feedwater nozzle was determined from the actual feedwater data. The number of startup/shutdown and scram events for Brunswick 2 was linearly projected based on actual plant operating history during the first fourteen years (1975-1988) of plant operation (References 2, 3 and 4).

The thermal boundary conditions used in this analysis differed from the Reference 1 document in that thermal sleeve annulus temperatures and annulus heat transfer coefficients, derived from Moss Landing test data (Reference 9), were used to calculate the thermal stresses in the feedwater nozzle.

To evaluate the crack growth, thermal and pressure stress analyses were conducted using the finite element computer code ANSYS (Reference 6). The locations of the peak thermal stress, peak pressure stress and peak combined thermal and pressure stress were determined and the crack growth was calculated using a crack growth computer code. The crack growth relationship used represents the 1989 ASME Section XI Code Curves. The best-fit correlation to actual PWR and BWR data used in the original revision of this report was not used, since the R-ratios (K_{min}/K_{max}) were typically high for the actual cycling data; that correlation is not valid for high R-ratios.

1.3.3 Nozzle Configuration

The initial revision of this report analyzed the replacement triple thermal sleeve sparger and machined feedwater nozzle bore. That replacement has not been implemented as of the current date, and there are currently no plans to do such. As a result, this report evaluates the original ring-interference fit, single thermal sleeve sparger design. The nozzle bore has not been machined and, as a result, was analyzed in its original clad configuration.

2. SUMMARY AND CONCLUSIONS

Application of the ASME Code, Section XI crack growth rate relationship resulted in crack growth greater than the acceptance criterion of 1 inch for a 40-yr plant life. The analysis resulted in a 1-inch crack depth after 32.3 years of operation.

This analysis is based on actual low flow feedwater controller characteristics obtained since the original revision of this report. Although this analysis did not show compliance with NUREG-0619, it should be noted that there are approximately 16 years of plant operation before the crack would reach the 1-inch depth.

The plant operating history is based on the initial 14 years of plant operation extrapolated to 40 years. Because of "learning curve" effects which are typically experienced by operating reactors during their initial years of operation, the extrapolation is most likely conservative. In addition, a potential exists for leakage past the thermal sleeve seal as a result of possible degradation of the seal from corrosion or some other form of relaxation. Therefore, conservative heat transfer rates were used in the thermal stress analysis to accommodate the potential for such leakage.

With regards to RWCU reroute, a plant-specific analysis was performed for Brunswick 2 which demonstrates that RWCU reroute leads to only a small improvement on thermal cycling and fatigue usage of the feedwater nozzle region (Reference 14). Based on that analysis, it was concluded that monitoring thermal sleeve seal leakage is more important than RWCU reroute.

Therefore, based on the results presented herein, it is proposed that this analysis be used as a basis for continued operation for the next several years until either the Brunswick 2 operating history can be updated or some form of leakage assessment can be made so that compliance with NUREG-0619 can be shown. Periodic examination, required by NUREG-0619, of the feedwater nozzle will provide additional justification that continued operation is acceptable.

3. THERMAL CYCLE DEFINITION

The feedwater nozzle thermal cycle definitions are represented by Figures 3-1 through 3-4. These figures represent the minimum and maximum temperature points for two startup/shutdown events and two scram events obtained from CP&L for Brunswick 2. The feedwater loop which has the RWCS mixing (Loop B) had the most severe cycling and was therefore used throughout this analysis.

The following events appropriate for [illegible] analysis were identified by CP&L personnel from the last available fuel cycle:

Figure	Date	Event Description
3-1	5/3/88	Shutdown/Startup
3-2	5/7/88	Forced Shutdown
3-3	11/16/89	Scram
3-4	6/17/89	Scram

These events are depicted in Figures 3-1 through 3-4, and were digitized into computer form from microfilmed strip chart recordings of feedwater temperature. The temperature measurements were taken by hardware installed subsequent to the original issue of this report, and are symbolic of the cycling occurring at the feedwater nozzle. The events are shown exactly as digitized from the microfilm recordings; consequently, actual progress of each event is from right to left.

A total projection of 183 startup/shutdown events and 403 scram events over the 40-year plant life was made for Brunswick 2 based on operating data obtained from the first 14 years of operation (References 2, 3 and 4). This projection was determined using the methodology of Reference 5 as follows:

Year	Time Period	Number of Startups/Shutdowns	Number of Scrams*
1	3/75 - 3/76	5	36
2	3/76 - 3/77	1	13
3	3/77 - 3/78	2	22
4	3/78 - 3/79	0	12
5	3/79 - 3/80	1	15
6	3/80 - 3/81	4	13
7	3/81 - 3/82	3	22
8	3/82 - 3/83	5	5
9	3/83 - 3/84	5	7
10	3/84 - 3/85	10	6
11	3/85 - 3/86	2	3
12	3/86 - 3/87	5	10
13	3/87 - 3/88	3	1
13.67	3/88 - 11/88	9	4

*Note: Turbine trips identified in Reference 4 were counted as scram events. Although conservative, this method of counting is not considered to be a significant contributor to the final crack growth results.

Based on the methods of Reference 5, the following equation is used to determine the projected number of events for the 40-year design life:

$$n_{40} = n_4 + n_{13.67}[36/(13.67-4)]$$

where n_{40} = projected number of events at year 40

n_4 = number of events at year 4

$n_{13.67}$ = number of events between year 4 and year 13.67

Thus, for startup/shutdown events, the following is obtained:

$$n_{40} = 8 + 47(36/9.67) = 183$$

and for scram events:

$$n_{40} = 83 + 86(36/9.67) = 403$$

For the purposes of this analysis, a single cycle is defined when the nozzle fluid temperature, initially at some value T_0, changes to some other value T_1 and then returns to T_0.

For the purposes of defining complete startup/shutdown cycles, the startup corresponding to the 5-7-88 forced shutdown event (Figure 3-2) was assumed to be a mirror image of (i.e., identical to) the shutdown event. Therefore, two (2) complete startup/shutdown cycles and two (2) complete scram cycles define the thermal cyclic duty experienced by Brunswick 2. These events were assumed to be typical for the entire 40-year design life of the reactor.

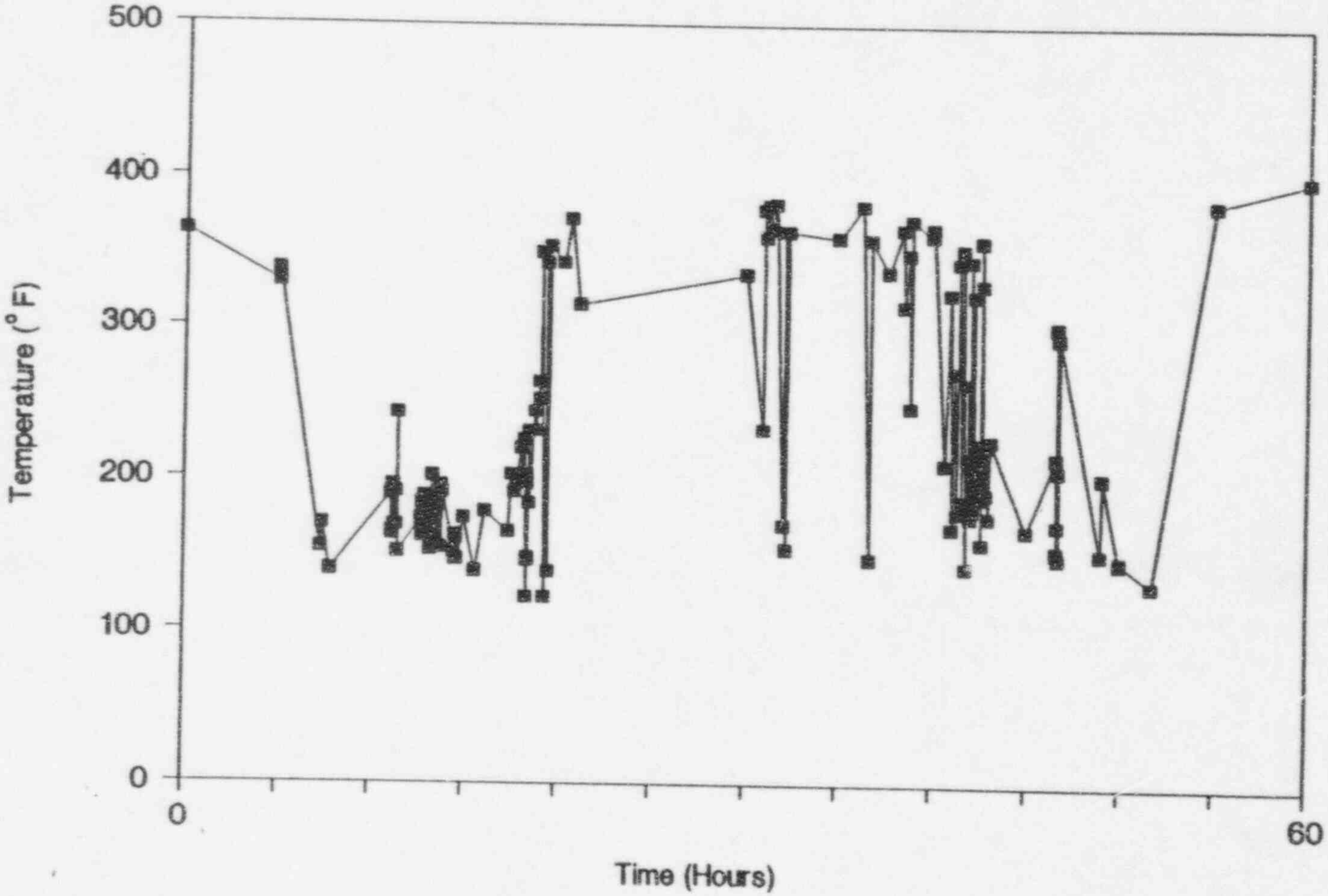


Figure 3-1. Temperature Cycling for 5/3/88 Shutdown-Startup

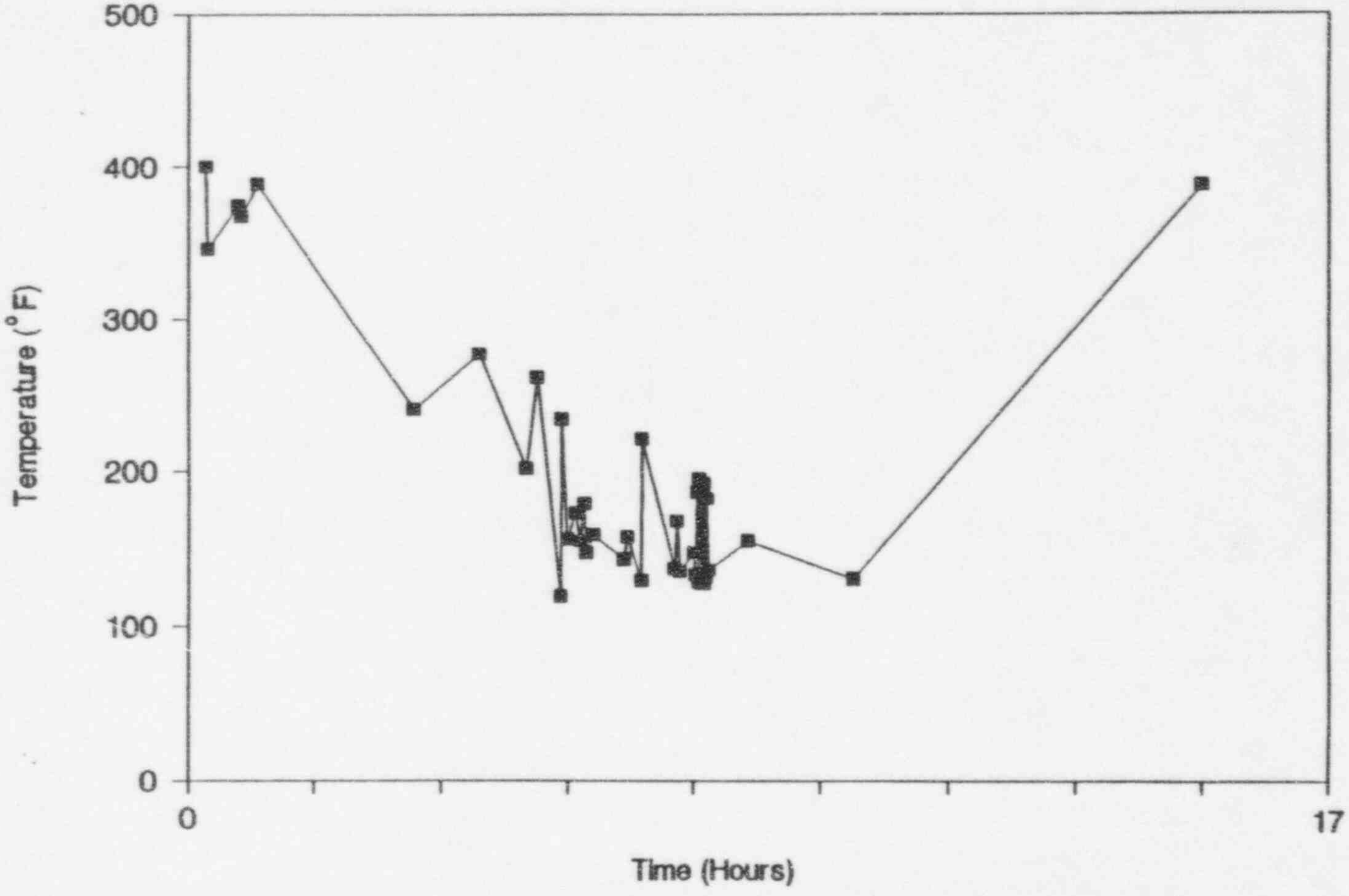


Figure 3-2. Temperature Cycling for 5/7/88 Forced Shutdown

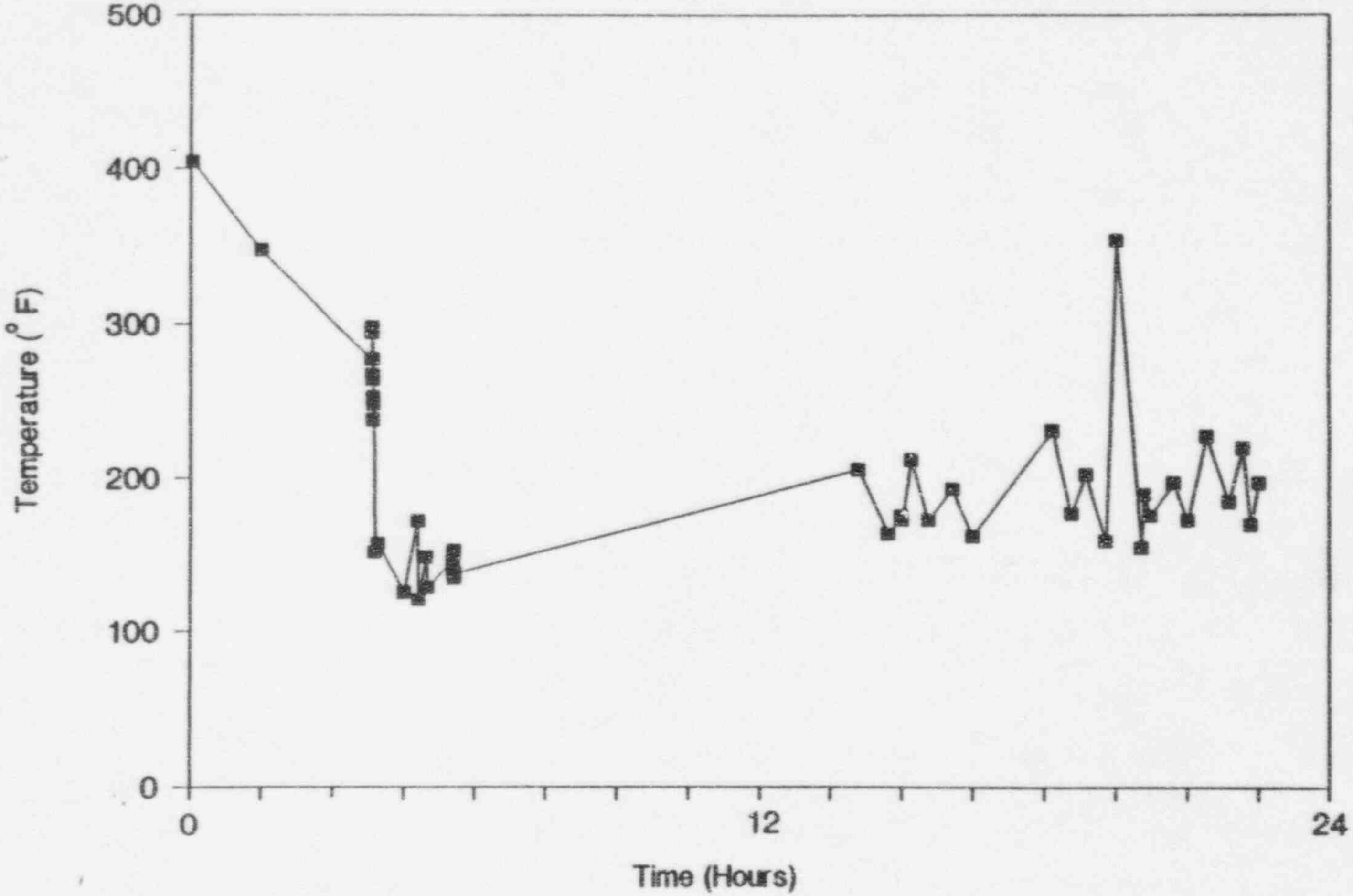


Figure 3-3. Temperature Cycling for 11/16/88 Scram

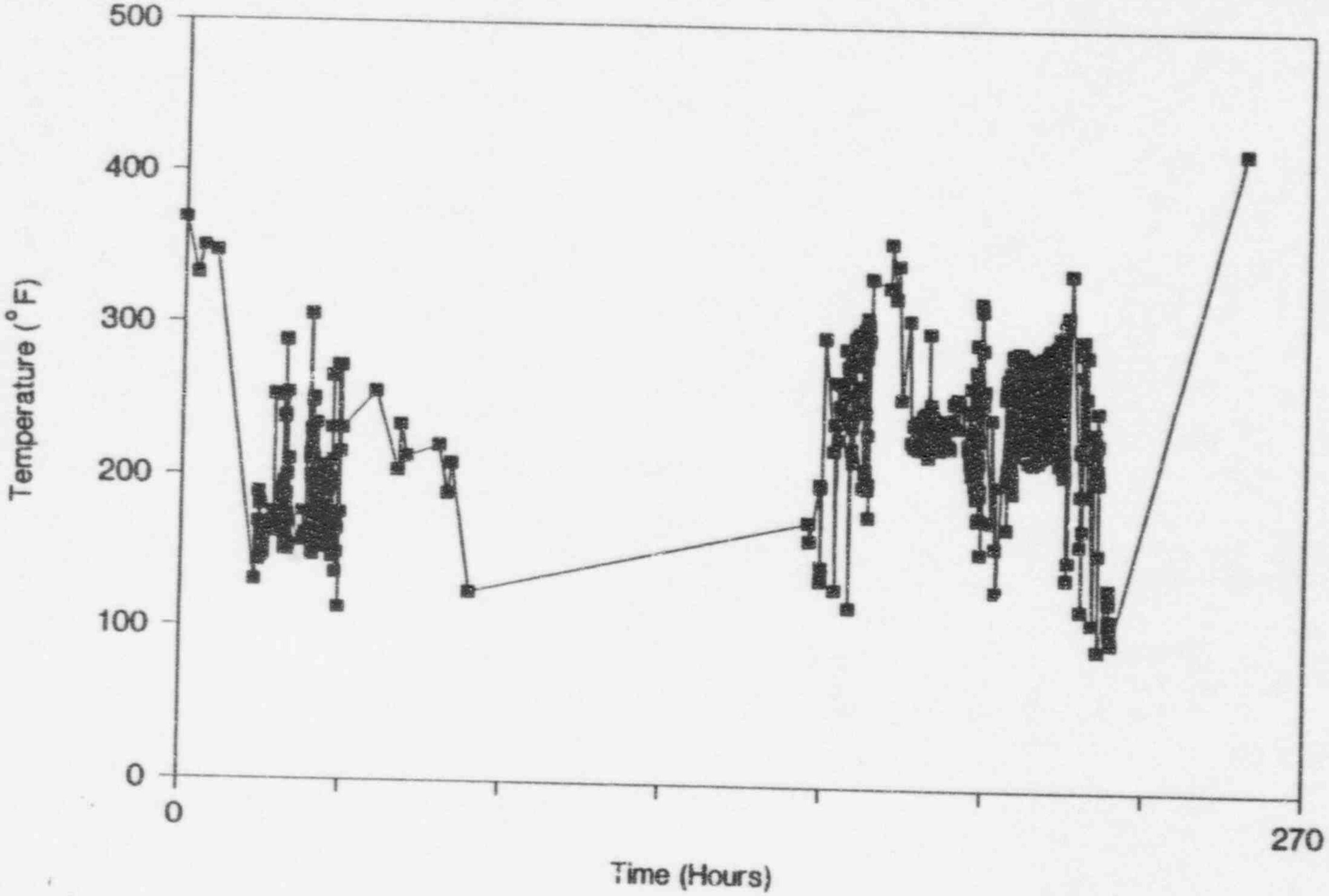


Figure 3-4. Temperature Cycling for 6/17/89 Scram

4. THERMAL ANALYSIS

The finite element computer code ANSYS (Reference 6) was used to develop an axisymmetric model which simulated the Brunswick 2 feedwater nozzle. The isoparametric heat conduction element (STIF 55) was used. The model was developed using the nozzle configuration shown in Figure 4-1 (References 7 and 8). The configuration depicted in Figure 4-1 is the original nozzle design without the cladding removed, as discussed in Section 1.3.3. The same model with an isoparametric stress element was subsequently used for the stress analysis. Further discussion of the model configuration is included in Section 5.

The heat transfer coefficients and temperature boundary conditions were derived from Reference 9. The method of derivation is explained in the Appendix to this report. The use of annular temperatures and heat transfer coefficients removed the necessity of specifically modeling the thermal sleeve in the finite element analysis. The feedwater nozzle thermal sleeve design is a single sleeve ring-interference fit to the feedwater nozzle safe end and welded to the feedwater sparger. These heat transfer coefficients with the appropriate temperature boundary conditions are shown superimposed upon a drawing of the finite element model in Figure 4-2.

The initiation of feedwater flow was modeled by varying the temperatures in Zones 2 and 3 from 550°F down to the temperatures indicated in Figure 4-2, over a 3-sec interval. The temperatures were maintained at this level until steady-state conditions were reached. The 3-sec ramp was used rather than a step change, since it is conservative and still assures numerical stability in the computer solution.

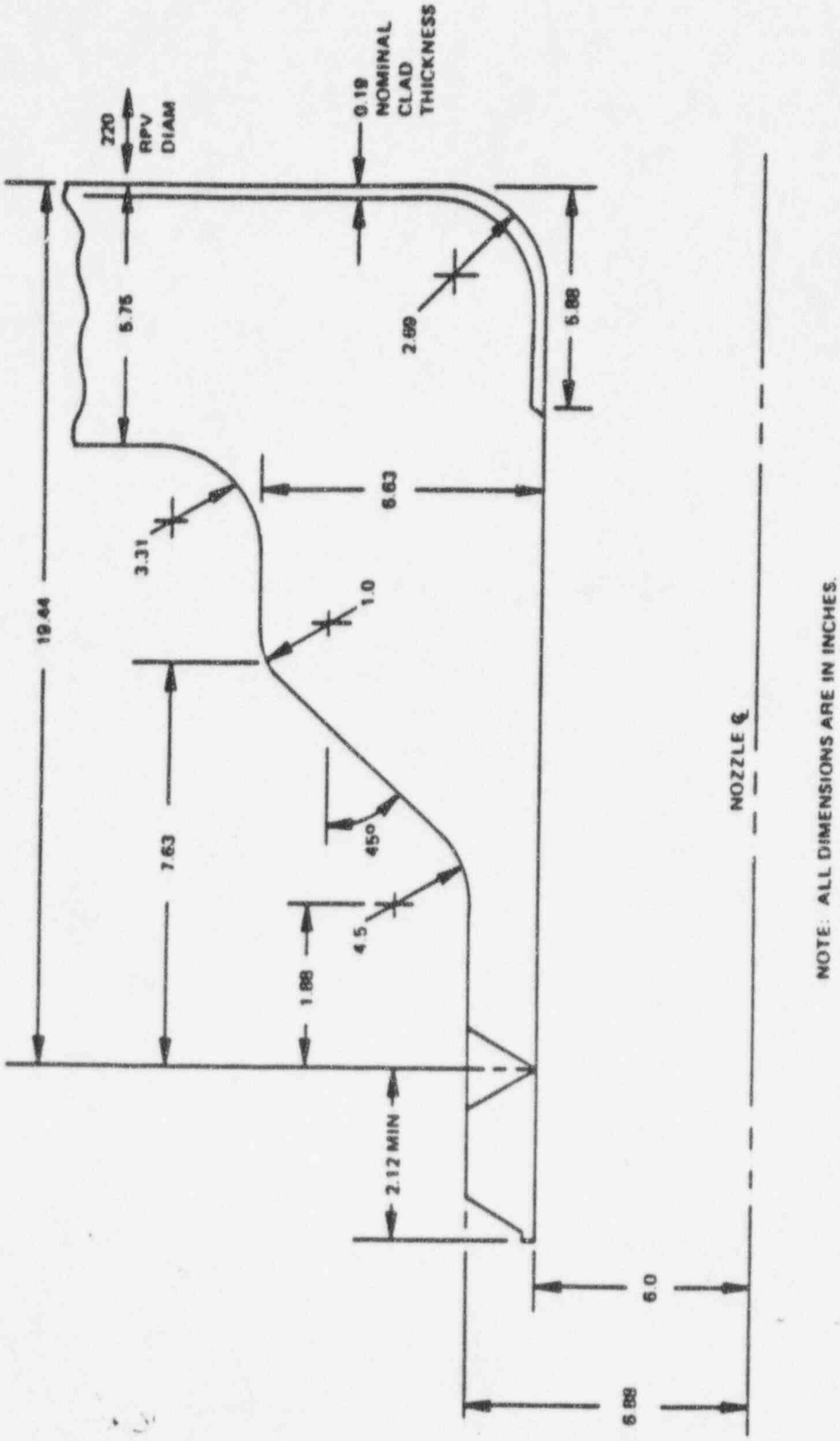


Figure 4-1. Nozzle Configuration

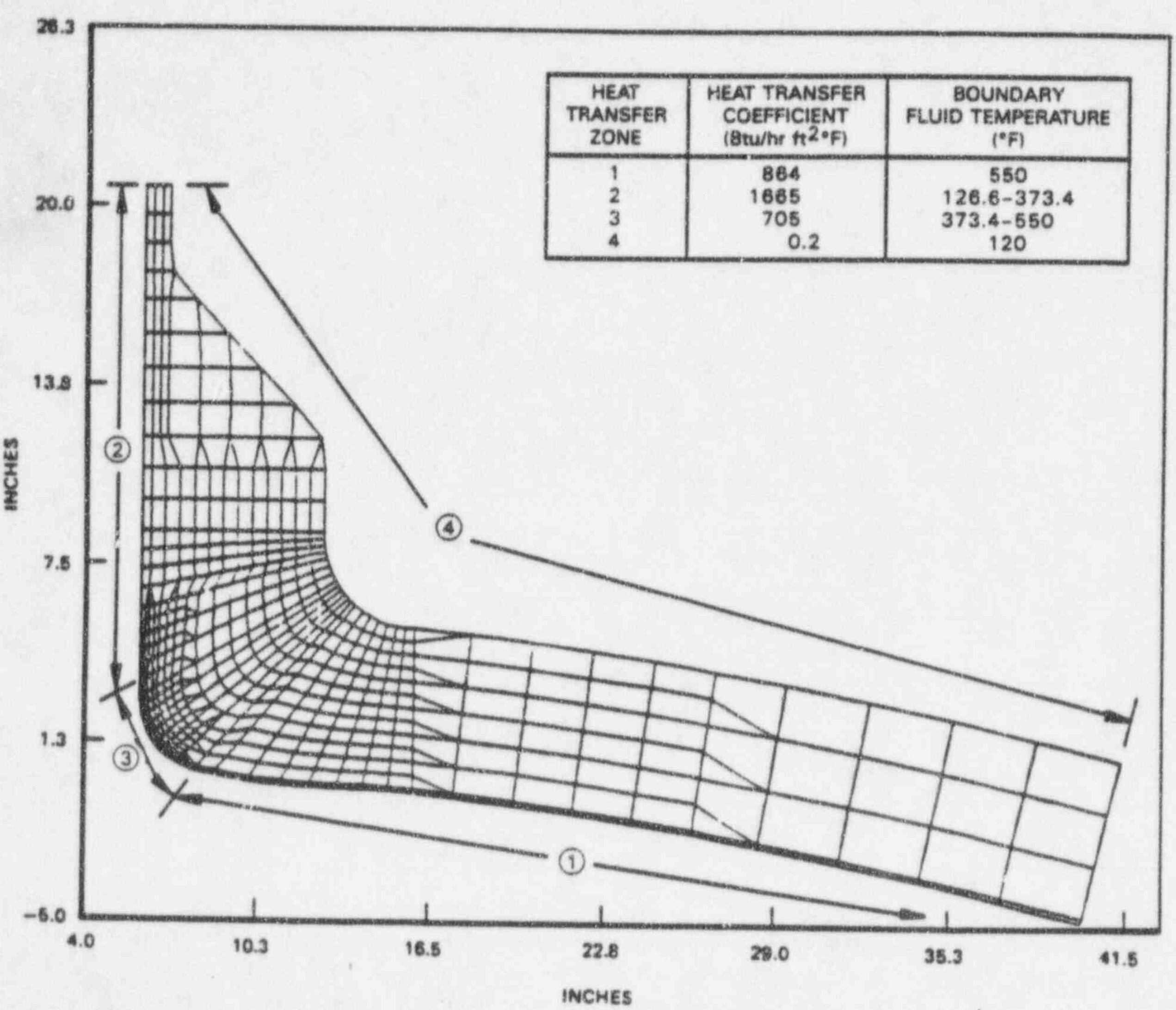


Figure 4-2. Thermal Boundary Conditions

5. THERMAL AND PRESSURE STRESSES

The results of the thermal analysis were applied to the previously mentioned finite element stress model to determine the thermal stresses. Isoparametric stress elements (STIF 42) were used in the stress analysis. The nozzle was modeled by an axisymmetric finite element mesh with the vessel being represented as a spherical shell. This is a common approximation used in the stress analysis of a three-dimensional nozzle configuration in a cylindrical shell. This was adequate for thermal stresses, but pressure stresses required a scaling factor based on a three-dimensional analysis. The lengths of the nozzle safe end and pressure vessel section were each modeled to at least $2.5\sqrt{rt}$, where r is the radius and t is the thickness of the nozzle. This was done to assure that end effects did not influence the stresses in the nozzle corner.

Thermal stresses were evaluated during several time intervals over the course of the transient by analyzing node pair temperature differences at various locations in the nozzle blend radius. Only the stresses occurring at 3 minutes were used in the subsequent crack growth analysis, since they resulted in the most limiting stress profile in the nozzle blend radius region. The highest thermal stress occurs on the inside surface of the nozzle blend radius as shown in Table 5-1. The thermal stresses which developed from a ΔT of 450°F were linearly scaled to the ΔT described in the thermal cycle definitions (Section 3). The scaled thermal stresses are subsequently used in the crack growth analysis.

The maximum thermal stresses occurred in the stainless steel cladding area of the nozzle blend radius (ends at Element 337; Figure 5-1).

Pressure stresses for the case of a 1000-psi vessel pressure were also calculated; however, these stresses required application of a scaling factor. This was necessary because of the limitation of modeling a three-dimensional component with a two-dimensional axisymmetric model. Because the three-dimensional characteristics near the nozzle corner were not modeled, the peak

Table 5-1

SURFACE STRESSES TO CHOOSE MAXIMUM COMBINED STRESSES
(Steady State)

Element	Thermal Hoop (psi)	Pressure Hoop (psi)	Pressure Ratioed to 1.5004 (psi)	Combined (psi)
169	-922	27196	40805	39883
185	7217	28391	42598	49815
193	17122	29065	43610	60732
209	26949	29519	44291	71239
217	35988	29725*	44600*	80588
233	43127	29661	44504	87631
241	49040	29295	43955	92994
257	54623	28664	43008	97631
265	59953	27663	41506	101459
281	64940	26293	39451	104391
289	68995	24462	36703	105698
305	72933	22808	34222	107155
313	76412	21232	31857	108269
329	79978	19821	29740	109717*
337	80273*	18380	27578	107851
353	56119	16725	25095	81213
369	52698	14798	22203	74901

*Maximum stress

Table 5-1

SURFACE STRESSES TO CHOOSE MAXIMUM COMBINED STRESSES (Continued)
(3 min after beginning of transient)

Element	Thermal Hoop (psi)	Pressure Hoop (psi)	Pressure Ratioed to 1.5004 (psi)	Combined (psi)
169	-3808	27196	40805	36998
185	5089	28391	42598	47688
193	15716	29065	43610	59326
209	26726	29519	44291	71017
217	37160	29725*	44600*	81760
233	45956	29661	44504	90460
241	53733	29295	43955	97688
257	61359	28664	43008	104367
265	68870	27663	41506	110376
281	77127	26293	39451	116577
289	84835	24462	36703	121538
305	92060	22808	34222	126281
313	98704	21232	31857	130561
329	105297	19821	29740	135036
337	108208*	18380	27578	135786*
353	88351	16725	25095	113445
369	88634	14798	22203	110837

*Maximum stress

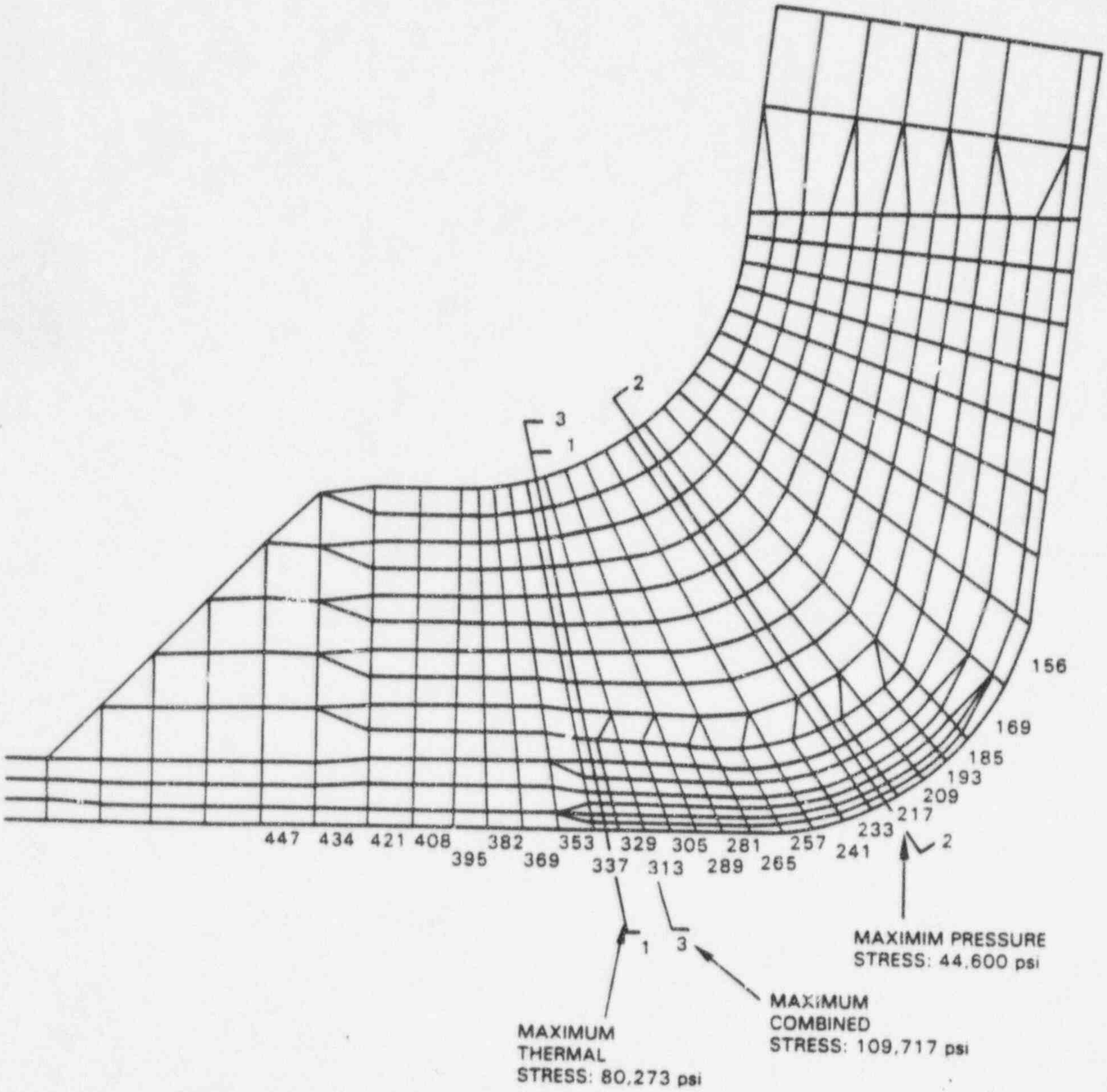


Figure 5-1. Feedwater Nozzle-Brunswick 2 Location of Maximum Surface Stress (Steady State)

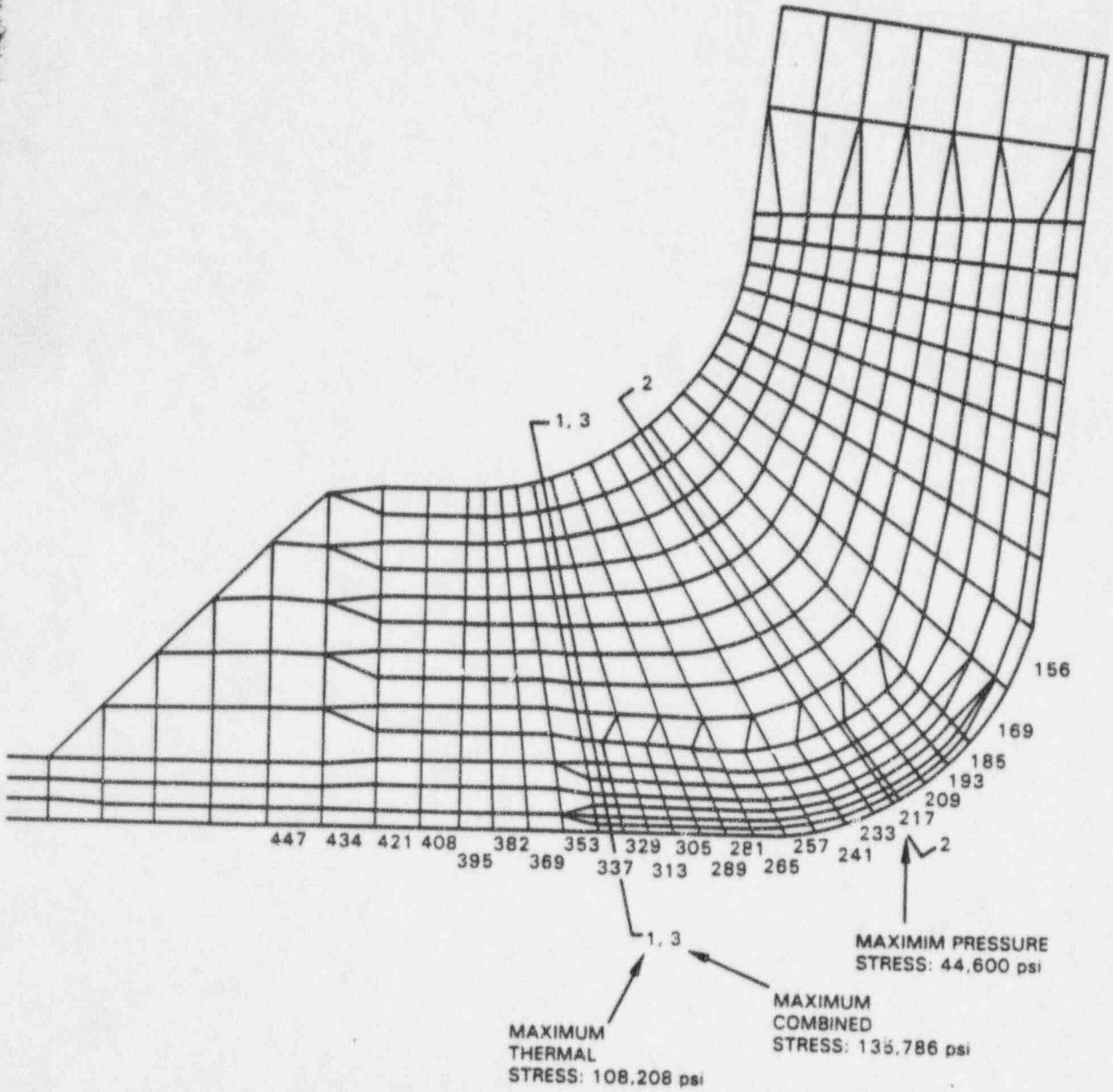


Figure 5-2. Feedwater Nozzle-Brunswick 2 Location of Maximum Surface Stress (Transient t = 3 minutes)

stresses at the nozzle corner were not accounted for accurately. Therefore, a generic three-dimensional model developed by Gilman and Rashid (Reference 10) was used to scale the pressure stress. The scaling factor for the pressure stress is given by the ratio of the peak pressure stress on the inside surface reported by Gilman and Rashid to the peak pressure stress on the inside surface from the finite element model used in this report. The peak pressure stress of the finite element model was 29,725 psi, while the peak pressure stress reported by Gilman and Rashid is 44,600 psi. This resulted in a scaling factor of 1.5004. The scaled peak pressure stress on the inside surface is shown in Figure 5-1.

The combined thermal and scaled peak pressure stresses were examined to determine the area where the combined peak stress on the inside surface occurs, as shown in Table 5-1. The stresses at the cross section associated with the limiting stress profile (see Table 5-2, and cross section 3-3 on Figure 5-2) were used to calculate crack growth.

Table 5-2

LIMITING STRESS PROFILE (CROSS SECTION 3-3)

Distance from Inside Surface (in.)	Pressure Hoop Ratioed to 1.5004 (psi)	Thermal Hoop 3 minutes (psi)
0.0	27578	135786
0.075	27029	130367
0.225	26325	96223
0.400	25242	88215
0.600	24161	79987
0.850	23049	69259
1.150	21849	59016
1.500	20587	47598
1.885	19843	36964
2.384	18253	26363
3.007	16571	16994
3.631	15084	10689
4.255	13705	6691
4.879	12360	4260
5.503	10976	2782

6. CRACK GROWTH ANALYSIS

6.1 STRESS INTENSITY FACTOR CALCULATIONS

Stress intensity factors were determined using solutions for standard stress distributions (e.g., constant, linear, quadratic, and cubic variations) and applying the superposition principle. The stress intensity solution for these unit load cases was expressed in terms of the crack length and appropriate magnification factors for the specific crack geometry (Figure 6-1). The stress intensity for an arbitrary stress distribution was then obtained by fitting a third-order polynomial of the form:

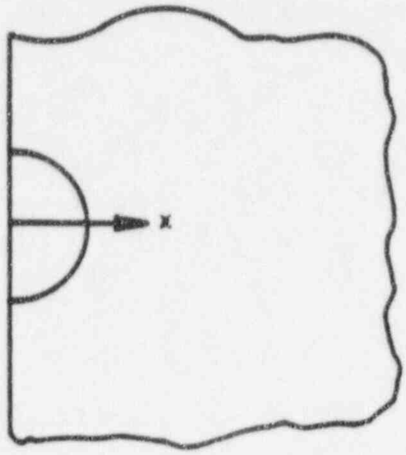
$$\sigma = A_0 + A_1X + A_2X^2 + A_3X^3$$

and applying the principle of superposition. Once the curve fit parameters A_0, A_1, A_2, and A_3 were known, the stress intensity factor was determined as a function of crack depth using the equations in Figure 6-1.

Magnification factors for several common two-dimensional geometries are available in References 11 and 12. For the feedwater nozzle, a set of three-dimensional magnification factors is presented in Reference 1. As illustrated in Figure 6-1, the nozzle corner factors (0.706, 0.537, 0.448, and 0.393) were obtained by averaging the magnification factors developed for circular surface crack geometries in half and quarter spaces. This expression (labeled FUN 11) was used to calculate stress intensity factors in the fracture mechanics evaluation which follows.

The pressure and thermal stress distributions were fit to third-order polynomials using a standard least squares procedure. Overall accuracy of the polynomial representations is considered more than adequate.

Substituting these polynomial coefficients (A_0, A_1, A_2 and A_3) into the FUN 11 stress intensity factor expression of Figure 6-1 leads to the stress intensity factor versus crack depth data shown in Figures 6-2 and 6-3. (These stress intensity factors apply to cross section 3-3.)

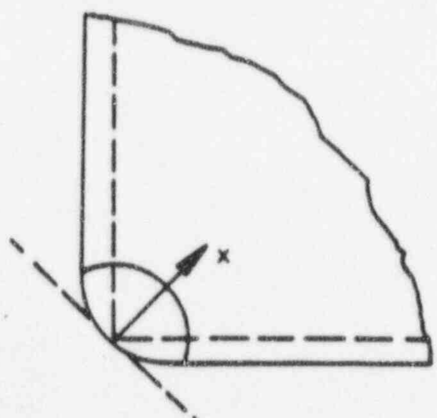


FUN 9 – SEMI-CIRCULAR CRACK IN HALF-SPACE

$$K_I = \sqrt{\pi a}\,(0.688\,A_o + 0.522\,(2a/\pi)\,A_1 + 0.434\,(a^2/2)\,A_2 + 0.377\,(4a^3/3\pi)\,A_3)$$

FUN 10 – QUARTER-CIRCULAR CRACK IN QUARTER-SPACE

$$K_I = \sqrt{\pi a}\,(0.723\,A_o + 0.551\,(2a/\pi)\,A_1 + 0.462\,(a^2/2)\,A_2 + 0.408\,(4a^3/3\pi)\,A_3)$$



FUN 11 – SIMULATED 3-D NOZZLE CORNER CRACK

$$K_I = \sqrt{\pi a}\,(0.706\,A_o + 0.537\,(2a/\pi)\,A_1 + 0.448\,(a^2/2)\,A_2 + 0.393\,(4a^3/3\pi)\,A_3)$$

Figure 6-1. Boundary Integral Equation/Influence Function Magnification Factors for BWR Feedwater Nozzle

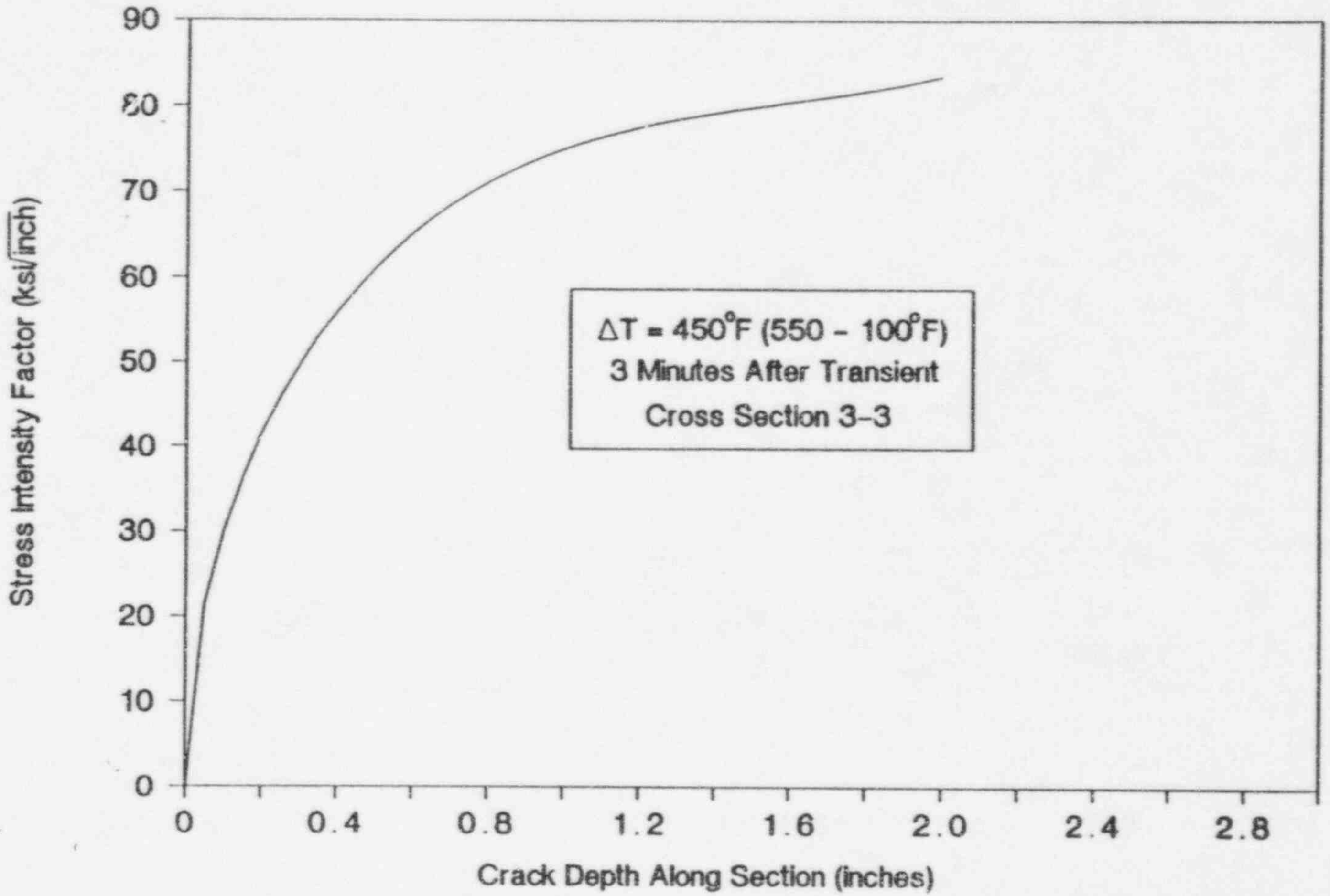


Figure 6-2. Stress Intensity Factor versus Crack Depth (Thermal Stresses, 3 minutes)

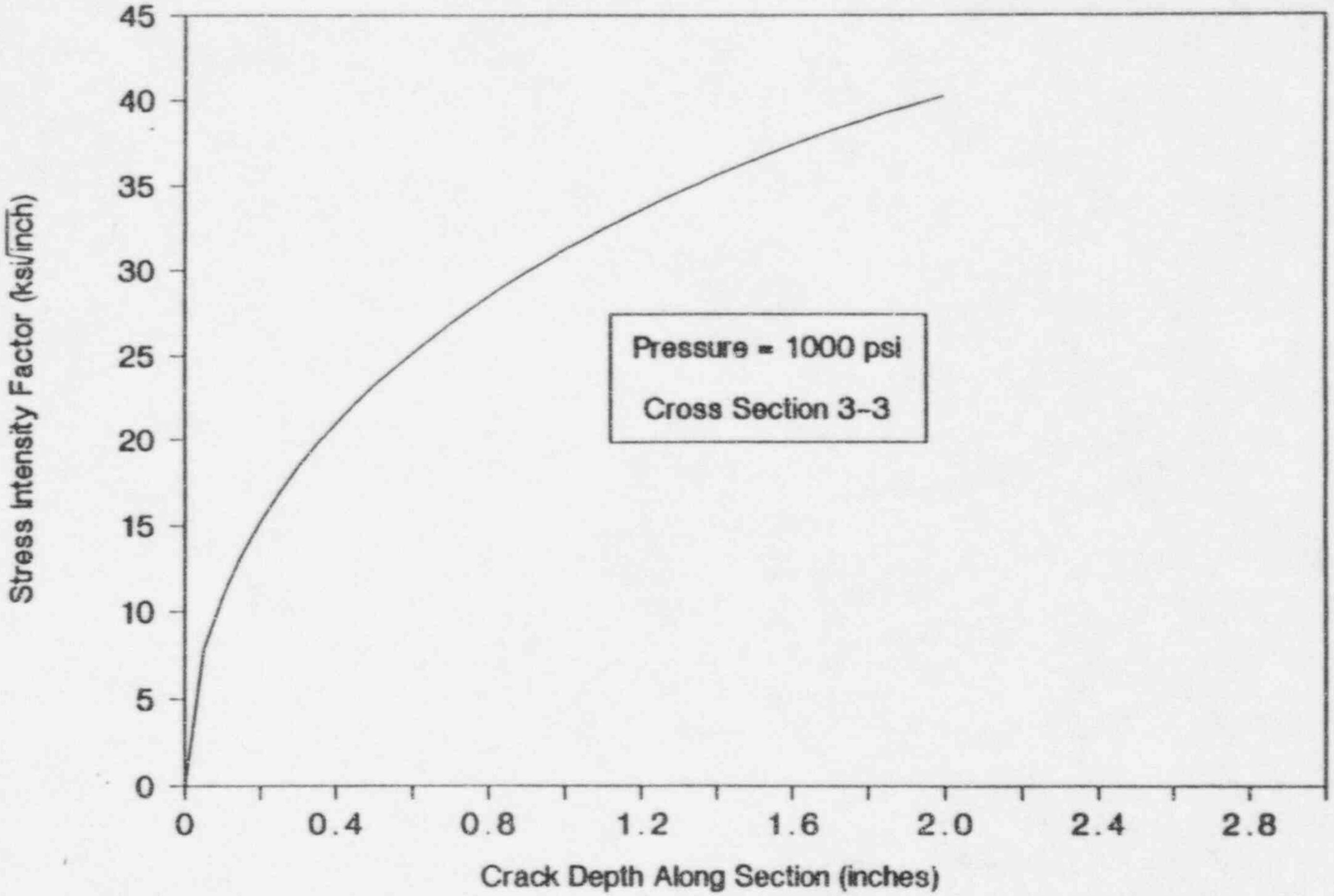


Figure 6-3. Stress Intensity Factor versus Crack Depth (Pressure Stresses)

6.2 CRACK GROWTH DATA

Figure 6-4 represents the fatigue crack growth data for low alloy steel from Section XI of the ASME Code (Reference 13). The R-ratio (K_{min}/K_{max}) dependence of this data is built-in by representing three cases: (1) R-ratio less than 0.25, (2) R-ratio between 0.25 and 0.65, and (3) R-ratio greater than 0.65. These data were used to determine the growth of an assumed 0.25-inch initial depth crack.

The best-fit compilation of fatigue crack growth data used in the original revision of this report was not used in the current analysis. That relationship is not valid for high values of R-ratio. Much of the data obtained for Brunswick 2 yielded high R-ratios (>0.9). As a result, unrealistic crack growth would result from the use of this relationship.

6.3 CRACK GROWTH EVALUATION

The thermal cycle definitions are represented by Figures 3-1 through 3-4 for startup/shutdown and scram/return to full power events. A projected total of 183 startup/shutdown events and 403 scram events was made for Brunswick 2 over the 40-year design life of the plant as described in Section 3.

The analysis conservatively assumed that the initial crack depth of 0.25 inch included the cladding thickness. Since the thermal stresses are higher in the stainless steel cladding region, the corresponding stress intensity factor would also be greater, thereby resulting in a more rapid crack growth propagation.

The procedure for calculating the crack propagation is as follows: for each cycle, the maximum and minimum stress and the number of occurrences were calculated. From this, the stress intensity factor range and corresponding R-ratio were calculated for the cycle being analyzed. Using this and the selected crack growth relationship, the incremental crack growth was

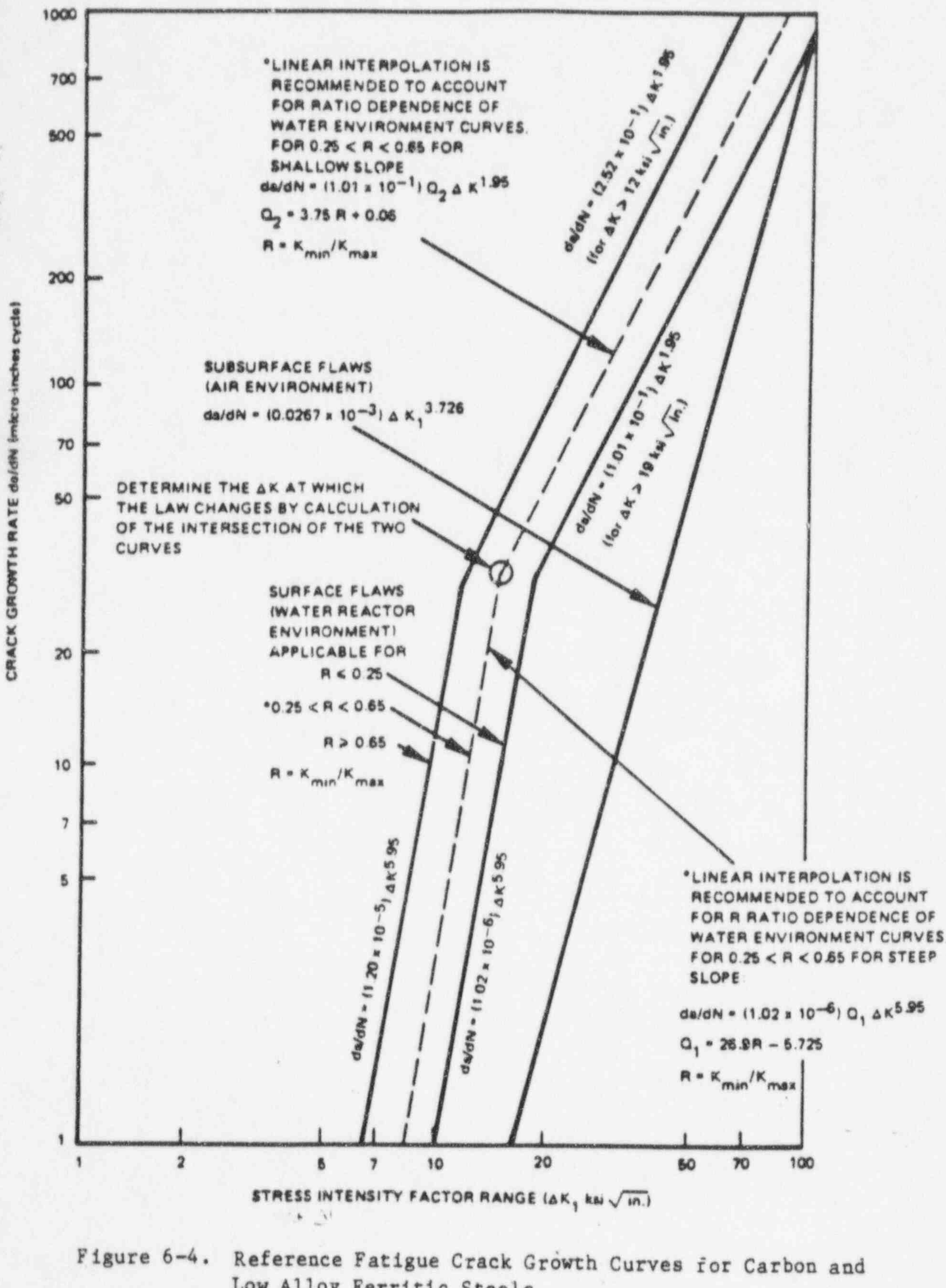


Figure 6-4. Reference Fatigue Crack Growth Curves for Carbon and Low Alloy Ferritic Steels

calculated for that event. The crack size was updated and the procedure repeated. This continued for every cycle until the entire life was analyzed.

The pattern of events was assumed to be:

25 sets of: 8 startup/shutdown events followed by
16 scram/return to full power events

Note that 25x8 = 200 startup/shutdowns and 25x16 = 400 scrams. The effects of modeling the seventeen extra startup/shutdown events and three less scram events is considered small.

The eight startup/shutdown events were further broken down as follows:

	4	5/3/88 Shutdown-Startup events (Figure 3-1)
	4	5/7/88 Forced Shutdown events (Figure 3-2) (including the mirror-image startup)
Total =	8	

The sixteen scram/return to full power events were further broken down as follows:

	8	11/16/88 Scram events (Figure 3-3)
	8	6/17/89 Scram events (Figure 3-4)
Total =	16	

One crack propagation calculation was made corresponding to the limiting stress profile shown in Table 5-2.

7. RESULTS AND CONCLUSIONS

Because of the recent acquisition of plant-unique data, the feedwater nozzle thermal cycle definitions defined in Section 3 were assumed to be representative of all startup/shutdown and scram events. The number of events over the plant life as projected using plant specific data (References 2, 3 and 4) was utilized. A plant-specific finite element stress analysis was performed for the feedwater nozzle.

The fracture mechanics analysis was based on the thermal stresses obtained from the finite element analysis, the thermal cycle definitions derived from actual plant feedwater data, and the historical frequency of the number of startup, scram and shutdown events.

The results of the fracture mechanics analysis are given in Figure 7-1 for the limiting location (cross section 3-3) as a function of the number of years since initial plant startup.

Using the 1989 ASME Section XI fatigue crack growth curves, the analysis shows that the postulated 0.25-in. crack becomes 1 inch deep 32.3 years after the startup date.

Even though compliance with NUREG-0619 cannot be shown at this time, there are approximately 16 years in which to update the Brunswick 2 operating history. The operating history used in the current analysis was based on an extrapolation of the initial 14 years of plant operation. Because of "learning curve" effects which are typically experienced by operating reactors during their initial years of operation, this extrapolation is most likely conservative (as recognized by the large number of startup/shutdown events). Therefore, this cycle counting information should be continually updated so that any conservatism present in the 40-year projection can be identified and eliminated. If a significant conservatism is identified, it may be possible to demonstrate compliance with the requirements of NUREG-0619.

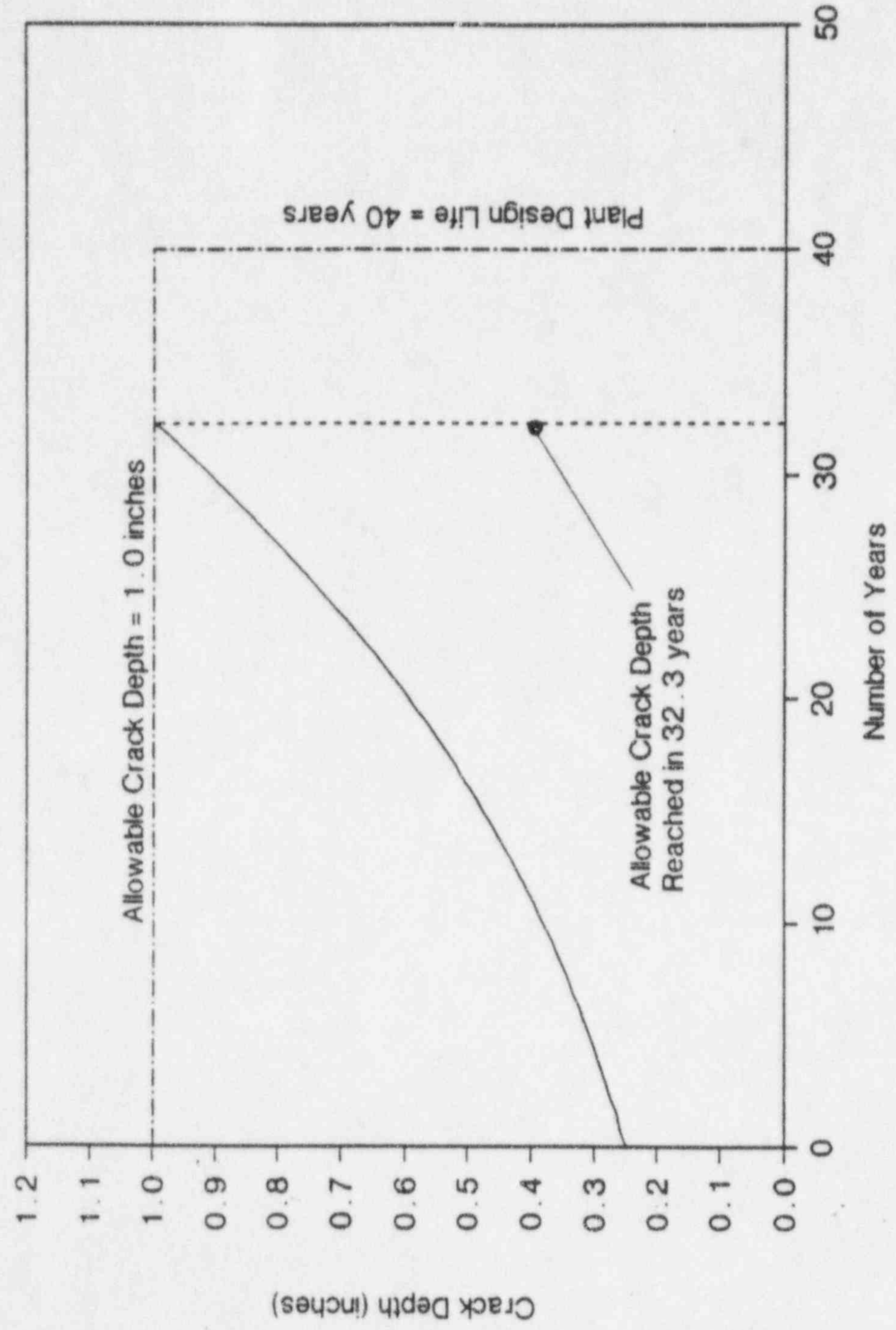


Figure 7-1. Crack Depth versus Number of Years

In addition, conservative heat transfer coefficients were used to accommodate the potential for possible leakage flow past the thermal sleeve seal (see Appendix). Although the Brunswick 2 configuration is a ring-interference fit, degradation of the seal is assumed to occur with time as a result of corrosion and other forms of relaxation. To account for this degradation, leakage flow was assumed in the analysis. Since this assumption has a significant impact on the resulting thermal stresses (as recognized by Brunswick 2 stresses being higher than Brunswick 1), it is recommended that some form of leakage assessment or monitoring be performed. This monitoring would provide an actual measurement of the leakage flow rate which could then be factored into this analysis as appropriate.

Based on the results of the above recommendations, the crack propagation could potentially be re-evaluated to show full compliance with NUREG-0619 requirements. During the interim period, the NUREG-0619 required examinations can provide justification for continued plant operation.

8. REFERENCES

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3. NEDO-22196, "Reactor Pressure Vessel Thermal Cycle Fatigue Evaluation for Brunswick Steam Electric Plant Units 1 and 2," General Electric Company, March 1983.

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APPENDIX
THERMAL BOUNDARY CONDITIONS

A.1 HEAT TRANSFER COEFFICIENTS

The annular heat transfer coefficients were developed from the data of Reference A-1 as follows. An allowance was made for potential leakage flow past the thermal sleeve seal as a result of degradation of the ring-interference fit. As a result, the data base consisted of two tests which were run at low feedwater flow and approximately 4 GPM leakage flow. The heat transfer coefficients during the tests were determined from eight heat flux meters mounted circumferentially around a section of the nozzle blend radius.

The highest heat transfer coefficient measured was taken and corrected to account for the difference in nozzle blend radius between the test sparger and the Brunswick 2 sparger. The Nusselt number, Nu, is proportional to the Reynolds number to the nth power, where n is typically 0.8. The Reynolds number is in turn directly proportional to the nozzle blend radius, R. Therefore, the Nusselt number is proportional to the nozzle blend radius raised to the 0.8 power. In equation form,

$$Nu \propto R^{0.8}$$

The heat transfer coefficient, h, is given by

$$h = Nu\ (k/R)$$

where k is the thermal conductivity of the fluid. Thus

$$h \propto R^{-0.2}$$

This proportionality is used to correct the heat transfer coefficient. In the tests of Reference A-1, $h = 1767$ Btu/hr-ft^2-°F and $R = 2$ inches. For the Brunswick 2 sparger, $R = 2.69$ inches. Therefore, $h = 1665$ Btu/hr-ft^2-°F.

A.2 BOUNDARY TEMPERATURE CONDITION

Boundary (or annulus) temperatures were taken from both of the afcrementioned two tests. The second test had two data samples taken, so a total of three data samples were available. The test data is expressed in terms of a normalized temperature which is equal to the difference of the annular fluid temperature and the feedwater temperature divided by the difference of the reactor temperature and the feedwater temperature. Readings are available at several circumferential locations at four sections of the nozzle. At each section, the lowest readings for each test were averaged to produce the final result.

The annulus fluid temperatures, as determined from these Moss Landing tests, are given in Table A-1 as a function of position of the ring-interference fit nozzle configuration. The expression for obtaining the annulus fluid temperature is as follows:

$$T = T_{FW} + C_1 (T_V - T_{FW})$$

where

T = arnulus fluid temperature (°F)
T_{FW} = feedwater temperature (°F)
T_V = vessel temperature (°F)
C_1 = coefficient from Table A-1

A.3 REFERENCE

A.1 NEDE-21659-1, C. M. Kwong and H. Choe, "Moss Landing Feedwater Nozzle/Sparger Test Data Files," February 1979.

Table A-1

C_1 COEFFICIENT
(GE Proprietary)

Location	C_1
F2	0.0257
A	0.0592
B	0.1578
C	0.6075

Use linear interpolation between locations as illustrated in Figure A-1.

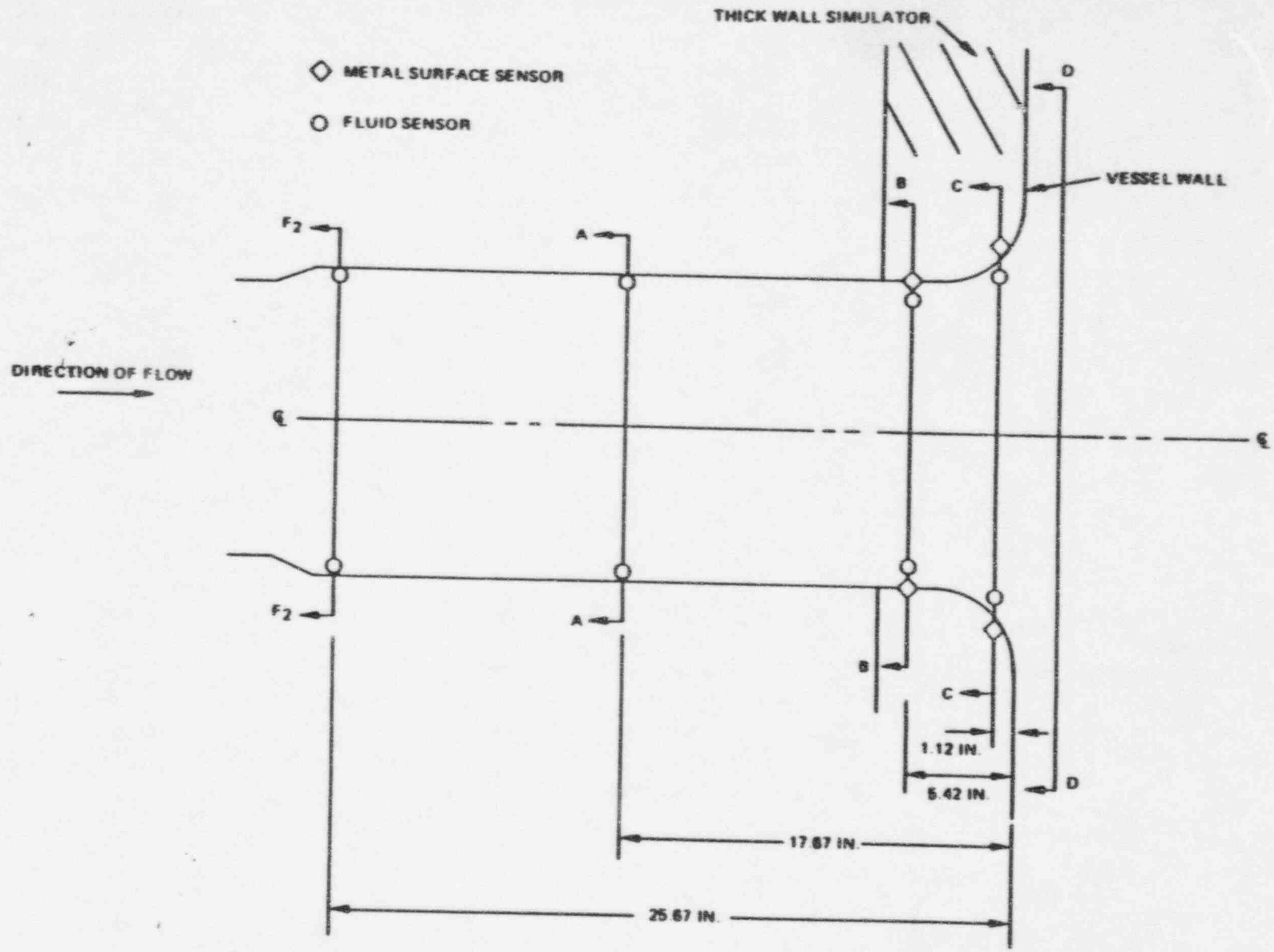

Figure A-1. Instrumentation of Feedwater Nozzle and Vessel Wall (from Moss Landing Tests, Reference A-1)

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