ENCLOSURE B

WCAP-10488

TECHNICAL BASIS FOR ELIMINATING PRESSURIZER SURGE LINE RUPTURES AS THE STRUCTURAL DESIGN BASIS FOR CATAWBA UNITS 1 & 2

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1.0 INTRODUCTION

1.1 BACKGROUND

The current structural design basis for the pressurizer surge line requires postulating non-mechanistic circumferential (guillotine) breaks in which the pipe is assumed to rupture along the full circumference of the pipe. This results in overly conservative estimates of support loads. It is, therefore, highly desirable to be realistic in the postulation of pipe breaks for the pressurizer surge line. Presented in this report are the descriptions of a mechanistic pipe break evaluation method and the analytical results that can be used for establishing that a guillotine type break will not occur within the pressurizer surge line. The evaluations considering circumferentially oriented flaws cover longitudinal cases.

1.2 SCOPE AND OBJECTIVE

The general purpose of this investigation is to show that a circumferential flaw which is larger than any flaw that would be present in the surge line will remain stable when subjected to the worst combination of plant loadings. The flaw stability criteria proposed for the analysis will examine both the global and local stability. The global analysis is carried out using the
[ ] method, based on traditional [ ] +a,c,e
concepts, but accounting for [ ] and taking into account the +a,c,e
presence of a flaw. This analysis using faulted loading conditions enables determination of the critical flaw size. The leakage flaw is conservatively selected with a length equal to half the critical length. The local stability analysis is carried out by performing a [+a,c,e
] of a straight piece of the surge line pipe containing a through-wall circumferential flaw subjected to internal pressure and external loadings (faulted conditions). The objective of the local analysis is to show that unstable crack extension will not result for a flaw [+a,c,e
] calculated by the global analysis.

The leak rate is calculated for the [ ] condition. [+a,c,e
] The crack +a,c,e
opening area resulting from [ ] loads is determined from an +a,c,e
assumed through-wall flaw of [ ] +a,c,e
[ ] is accounted for in determining the leak rate through this +a,c,e
crack. The leak rate is compared with the detection criterion of 1 gpm (Reg. Guide 1.45). The leak rate prediction model is an [+a,c,e

] This method was used earlier to estimate the leak rates through postulated cracks in the PWR primary coolant loop. [1-1]

1.3 REFERENCES

1-1 Palusamy, S. S. and Hartmann, A. J., "Mechanistic Fracture Evaluation of Reactor Coolant Pipe Containing a Postulated Circumferential Through-Wall Crack", WCAP-9570 Rev. 2, Class 3, June 1981, Westinghouse Nuclear Energy Systems.

2.0 FAILURE CRITERIA FOR FLAWED PIPES

2.1 GENERAL CONSIDERATIONS

Active research is being carried out in industry and universities as well as other research organizations to establish fracture criteria for ductile materials. Criteria, being investigated, include those based on J integral initiation toughness, equivalent energy, crack opening displacement, crack opening stretch, crack opening angle, net-section yield, tearing modulus and void nucleation. Several of these criteria are discussed in a recent ASTM publication [2-1].

A practical approach based on the ability to obtain material properties and to make calculations using the available tools, was used in selecting the criteria for this investigation. The ultimate objective is to show that the pressurizer surge line containing a conservatively assumed circumferential through-wall flaw is stable under the worst combination of postulated faulted and operating condition loads within acceptable engineering accuracy. With this viewpoint, two mechanisms of failure, namely, local and global failure mechanisms are considered.

2.2 GLOBAL FAILURE MECHANISM

For a tough ductile material if one assumes that the material is notch insensitive then the global failure will be governed by plastic collapse. Extensive literature is available on this subject. The recent PVRC study [2-2], reviews the literature as well as data from several tests on piping components, and discusses the details of analytical methods, assumptions and methods of correlating experiments and analysis.

A schematic description of the plastic behavior and the definition of plastic load is shown in Figure 2-1. For a given geometry and loading, the plastic load is defined to be the peak load reached in a generalized load versus displacement plot and corresponds to the point of instability.

A simplified version of this criterion, namely, net section yield criterion has been successfully used in the prediction of the load carrying capacity of pipes containing gross size through-wall flaws [2-3] and was found to correlate well with experiment. This criterion can be summarized by the following relationship:

$$Wa < Wp \qquad (2\text{-}1)$$

where Wa = applied generalized load
Wp = calculated generalized plastic load

In this report, Wp will be obtained by an [+a,c,e

]

2.3 LOCAL FAILURE MECHANISM

The local mechanism of failure is primarily dominated by the crack tip behavior in terms of crack-tip blunting, initiation, extension and finally crack instability. The material properties and geometry of the pipe, flaw size, shape and loadings are parameters used in the evaluation of local failure.

The stability will be assumed if the crack does not initiate at all. It has been accepted that the initiation toughness, measured in terms of J_{IN} from a J-integral resistance curve is a material parameter defining the crack initiation. If, for a given load, the calculated J-integral value is shown to be less than J_{IN} of the material, then the crack will not initiate.

If the initiation criterion is not met, one can calculate the tearing modulus as defined by the following relation:

$$T_{app} = \frac{dJ}{da} \frac{E}{\sigma_f^2} \qquad (2\text{-}2)$$

where T_{app} = applied tearing modulus
E = modulus of elasticity
σ_f = flow stress = [ ] +a,c,e
a = crack length
[+a,c,e
]

In summary, the local crack stability will be established by the two step criteria:

$$J < J_{IN}, \text{ or} \tag{2-3}$$

$$T_{app} < T_{mat}, \text{ if } J > J_{IN} \tag{2-4}$$

2.4 CORROSION

The Westinghouse reactor coolant system primary loop has an operating history (over 400 reactor years) which demonstrates its inherent stability characteristics. Additionally, there is no history of cracking in RCS primary loop piping. In addition to the fracture resistant materials used in the piping system, the chemistry of the reactor coolant is tightly controlled.

As stated above, the reactor coolant chemistry is maintained within very specific limits. For example, during normal operation, oxygen in the coolant is limited to less than [ ] This stringent oxygen limit is achieved +a,c,e by controlling charging flow chemistry and maintaining hydrogen in the reactor coolant at a concentration of [ ] The oxygen concentration +a,c,e in the reactor coolant is verified by routine sampling and chemical analysis. Halogen concentrations are also stringently controlled by maintaining concentrations of chlorides and fluorides at or below [ ]. This +a,c,e concentration is assured by controlling charging flow chemistry and specifying proper wetted surface materials. Halogen concentrations are also verified by routine chemical sampling and analysis.

2.5 REFERENCES

2-1 J. D. Landes, et al., Editors, Elastic-Plastic Fracture, STP-668, ASTM, Philadelphia, PA 19109, November 1977.

2-2 J. C. Gerdeen, "A Critical Evaluation of Plastic Behavior Data and a Unified Definition of Plastic Loads for Pressure Components," Welding Research Council Bulletin No. 254.

2-3 Mechanical Fracture Predictions for Sensitized Stainless Steel Piping with Circumferential Cracks, EPRI-NP-192, September 1976.

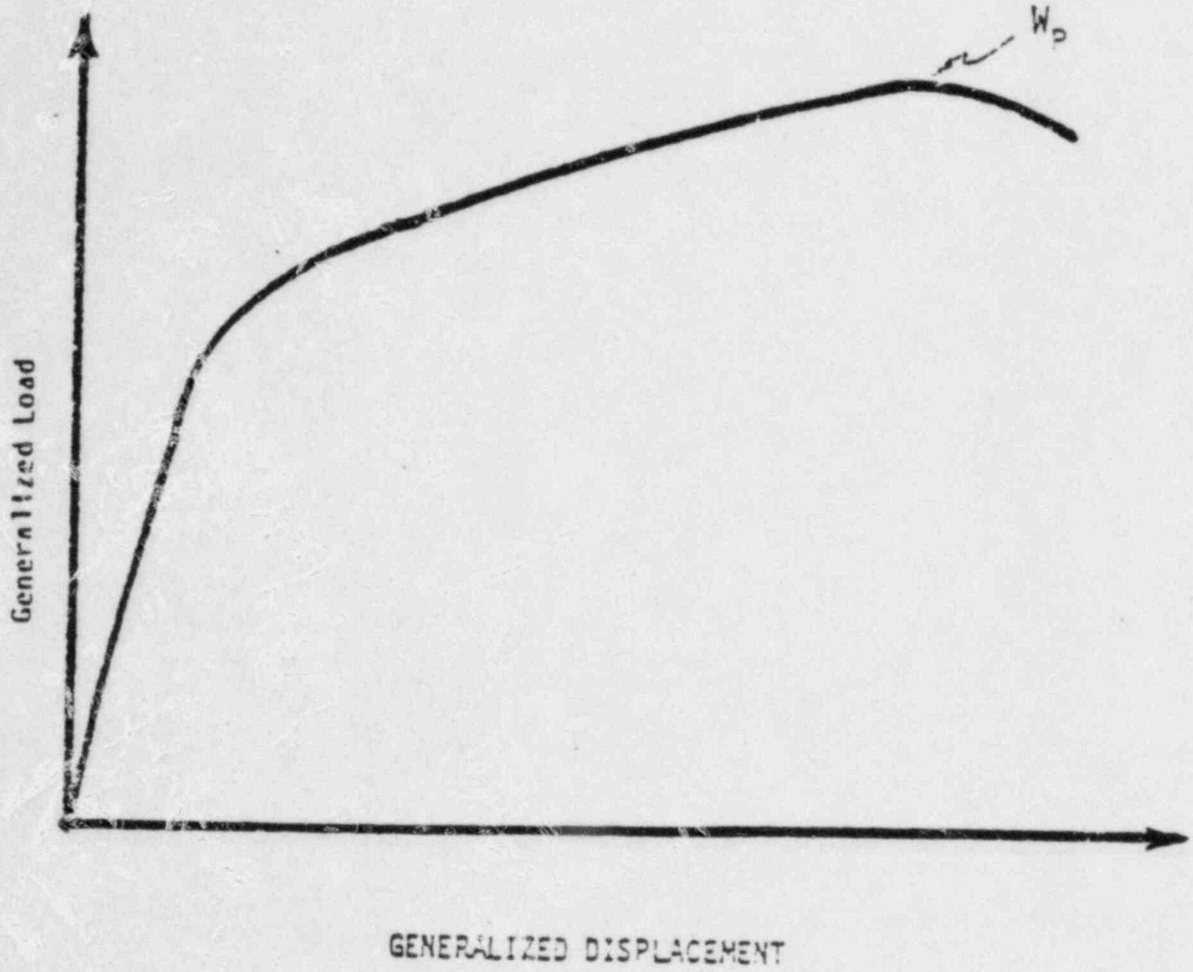


FIGURE 2-1 Typical Load - Deformation Behavior

3.0 LOADS FOR CRACK STABILITY ANALYSIS

The surge line stress report [3-1] was reviewed to identify locations with high ASME NB 3600 faulted (Eq. 9) stresses. These locations are identified on the surge line computer model in Figure 3-1. The loads at each of these locations were tabulated from the computer runs of [3-1] for the [ ] +a,c,e loading cases. The axial load, bending moment and stress at these locations were calculated from the tabulated loads as follows:

F = [ ] (3.1) +a,c,e

M_Y = [ ] (3.2) +a,c,e

M_Z = [ ] (3.3) +a,c,e

$$M = \sqrt{M_y^2 + M_z^2} \quad (3.4)$$

$$\sigma = \frac{F}{A} + \frac{M}{Z} \quad (3.5)$$

where,

subscript [DW, TH, SSE, AM] indicate the loading cases,

F_p = axial load due to normal operating pressure

M_y = Y component of moment

M_z = Z component of moment

F = total axial load at the location

M = bending moment at the location

A = metal cross-sectional area of piping

Z = sectional modulus of pipe

The wrought piping material is the same, (i.e., [ ] for the entire surge line and hence the location with the highest stress calculated by equation (3.5) was identified as the worst location for the global and the local crack stability analysis of Section 4.0 and 5.0, respectively. The [ ] was selected as the critical section based on this criteria (see Table 3-1). The calculated axial load, bending moment and longitudinal stress at this location are: +a,c,e

Axial Force	
Bending Moment	
Longitudinal Stress	

+a,c,e

[ ] +a,c,e

The operating transients of the surge line are such that no [ ] +a,c,e

REFERENCES

3-1 EDS Report No. 03-0093-1025, Revision 0, "ASME Boiler and Pressure Vessel Code Section III Class 1 Stress Report for the RCS Pressurizer Surge Line, Catawba Nuclear Station Unit 1."

+a,c,e

FIGURE 3-1: CATAWBA SURGE LINE PIPING ANALYSIS MODEL

TABLE 3-1

A SUMMARY OF CATAWBA SURGE LINE LOCATIONS
WITH HIGH LOADS AND STRESSES

Node No.	Axial Force (k)	Bending Moment (ft.-k)	Stress σ (ksi)

+a,c,e

4.0 CRITICAL FLAW SIZE CALCULATION

The conditions which lead to failure in stainless steel must be determined using plastic fracture methodology because of the large amount of deformation accompanying fracture. A conservative method for predicting the failure of ductile material is the [+a,c,e

] The flawed pipe is predicted to fail when the [+a,c,e

] This methodology has been shown to be applicable to ductile piping through a large number of experiments, and will be used here to predict the critical flaw size in the pressurizer surge line. The failure criterion has been obtained by [+a,c,e

] The detailed development is provided in Appendix A, for a through-wall circumferential flaw in a pipe with [+a,c,e

] The [ ] for these conditions is:

+a,c,e

[ ]

The analytical model described above accurately accounts for the piping internal pressure as well as imposed axial force as they affect the [ ] In order to validate the model, analytical predictions were compared with the experimental results [4-1] as shown in Figure 4-2. Good agreement was found. +a,c,e

In order to calculate the critical flaw size, a plot of the [ ] versus crack length is generated as shown in Figure 4-3. The critical flaw size corresponds to the intersection of this curve and the maximum load line. +a,c,e

The critical flaw size is [ ] using ASME Code [4-2] [ ] stainless steel. +a,c,e

Since [ ] for crack smaller than [ ] and [ ] the global stability criterion of Section 2.0 is satisfied. +a,c,e

Reference

4-1 Kanninen, M. F., et al., "Mechanical Fracture Predictions for Sensitized Stainless Steel Piping with Circumferential Cracks" EPRI NP-192, September 1976.

4-2 ASME Section III, Division 1-Appendices, 1983 Edition, July 1, 1983.

+a,c,e

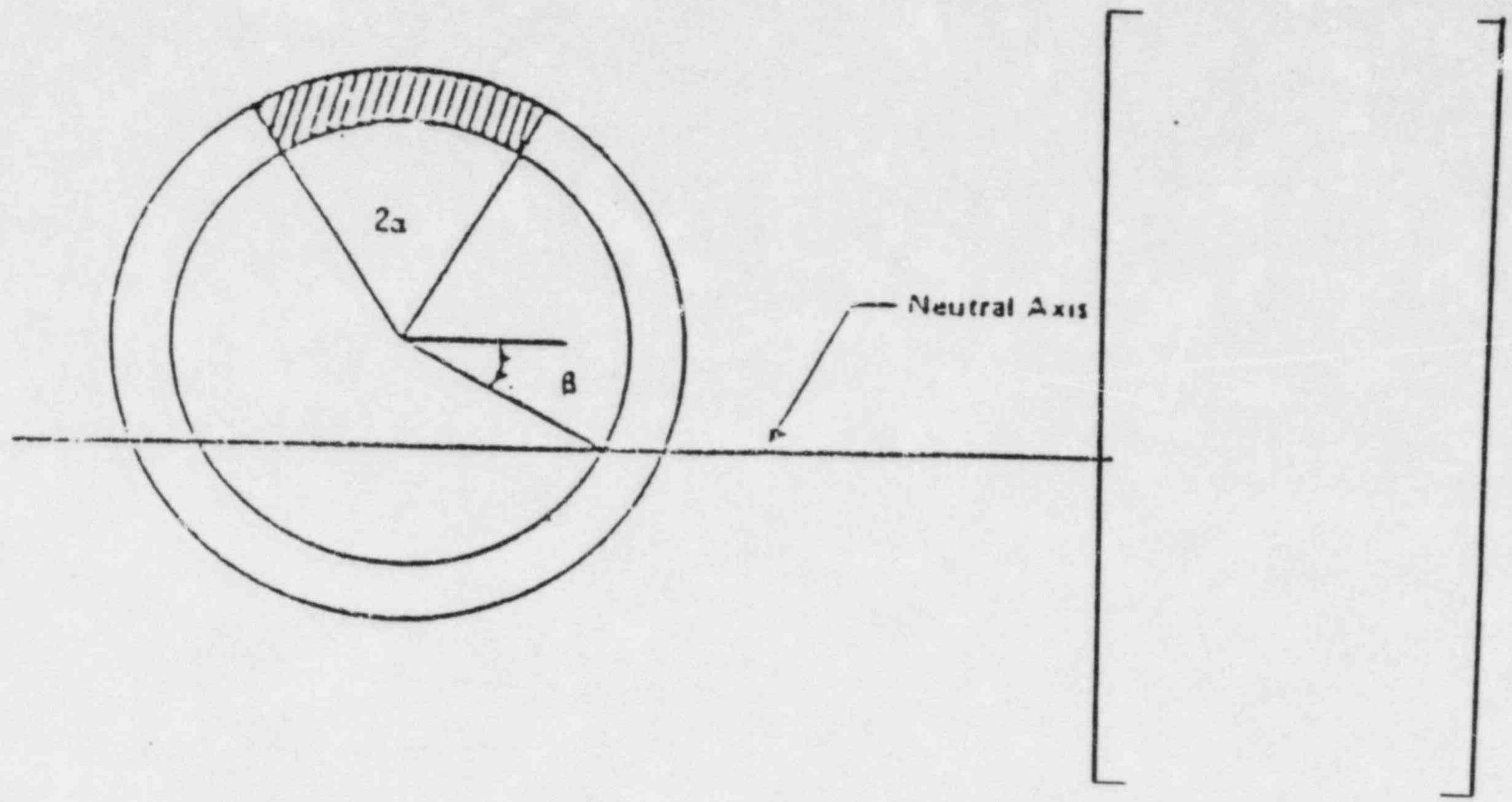


Figure 4-1 [ ] Stress Distribution

+a,c,e

+a,c,e

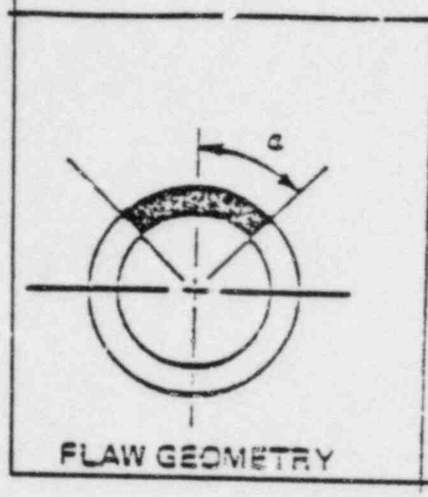
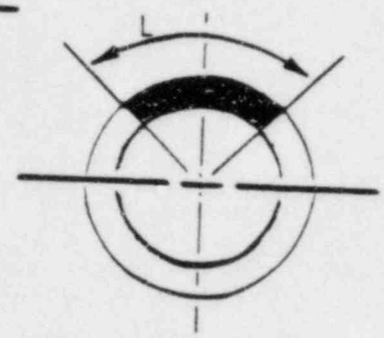


FIGURE 4-2 Comparison of [Limit Moment] Predictions With Experimental Results +a,c,e

PRESSURIZER SURGE LINE USING ASME CODE MINIMUM PROPERTIES

+a,c,e



FLAW GEOMETRY

+a,c,e

Figure 4-3 Critical Flaw Size for Pressurizer Surge Line

5.0 FINITE ELEMENT ANALYSIS FOR CRACK STABILITY CALCULATIONS

Using the [ ] computer program, a [ ] crack was analyzed to determine the local stability.[a] The loadings consist of [ ] +a,c,e

5.1 THE [FINITE ELEMENT] MODEL AND THE MATERIAL PROPERTIES

Figure 5-1 identifies all the loads acting on the pipe. The pipe thickness is [ ], based on the thinnest location of the surge line under investigation. The outer diameter is [ ]. Due to symmetry only one half of the circumference, i.e., 180-degree, is modeled. The length of the model is [ ] which is sufficiently long to attenuate the effect of the crack for correct boundary load input from the pipe end. Figures 5-2 through 5-7 all show the [ ] used for analysis. The [ ] are identified in Figure 5-2 through 5-5. The [ ] of interest for later leak rate predictions are shown in detail on Figure 5-6. The [ ] and their Z-coordinates required for the application of the axial loads and the bending moment are shown in Figure 5-7. +a,c,e

[ ] +a,c,e

The true stress-strain curve of the material is shown in Figures 5-8. The data are taken from the "Nuclear System Materials Handbook [5-2] for the stainless steel [ ] The stress-strain curve is [ ] It has been shown that the [bi-linear] approximation gives good agreement with the experimental results +a,c,e

---

a [ ] +a,c,e

[5-3]. The material properties used in the present analysis are [+a,c,e

]

5.2 BOUNDARY CONDITIONS AND METHOD OF LOADING

The boundary conditions are described in Figure 5-9. The pipe is subjected to the internal pressure of [ ] and an axial load of [ ] A bending moment of [ ] is then superposed to the pipe while the pressure and the axial loads are held constant. Due to non-linear material behavior, the loads are added to the pipe [ ] +a,c,e +a,c,e +a,c,e

Figures 5-10, 5-11 and 5-12 show the sequence of applying the loads to the [ ] model of the pipe. Figure 5-10 shows [ ] after which it is held steady. As shown in Figure 5-11, the axial load due to [ ] Figure 5-12 shows application of the moment, starting at load step 3, where [ ] +a,c,e +a,c,e +a,c,e

5.3 METHOD OF ANALYSIS

As mentioned in Section 2 of the present report the local instability criterion is based on the information of the J-integral, J, and the tearing modulus, T. The tearing modulus is defined in Equation 2-2. When the J-value due to the applied loads equals and exceeds a critical value, J_{IC} or J_{IN} of the material, the crack initiation will occur. When the T-value due to the applied loads equals and exceeds the T-value of the material, T_{mat}, the instability will occur.

[ ] This method has been successfully used to analyze a cracked pipe under a combined axial load and bending moment [5-5]. +a,c,e

The VCE method has been incorporated in the [+a,c,e
] to calculate the average [ ] of a crack as well as the +a,c,e
[ ] along the crack front for both [
] analyses. The [ ] at each load level can be computed by way +a,c,e
of the [ ] solution strategy. +a,c,e

5.4 [FINITE ELEMENT] RESULTS

It should be noted that J_{IN} (or J_{Ic}) and T_{mat} refer to two stages of a fracture process of a material. If the applied J-value is less than J_{IN}, the crack will not advance. Under this condition the tearing modulus need not be evaluated. Figure 5-13 shows how the calculated value of the J integral increases up to and beyond the maximum operating loading at 653°F. At the maximum loading, the [ ] has a corresponding value of [+a,c,e
] as shown on the figure. Since this value is smaller than the
[ ] the condition for +a,c,e
crack stability is automatically fulfilled. [+a,c,e

] The [ ] as a function of +a,c,e
loads is shown in Figure 5-13 and Table 5.1. The verification of the analysis is shown in Appendix B.

5.5 REFERENCES

5-1 [ ] +a,c,e

5-2 Nuclear System Materials Handbook, Volume I Design Data, Revision 1, 10/1/76.

5-3 Palusamy, S. S., Hartmann, A. J., "Mechanistic Fracture Evaluation of Reactor Coolant Pipe Containing a Postulated Circumferential Through Wall Crack," WCAP 9558, Revision 2, dated May 1982.

5-4 Parks, D. M., "The Virtual Crack Extension Method for Nonlinear Material Behavior," Computer Methods in Applied Mechanics and Engineering," Vol. 12, 1977, pp. 353-364.

5-5 Yang, C. Y. and S. S. Palusamy, "VCE Method of J Determination for a Pressurized Pipe Under Bending," J. of Pressure Vessel Technology, Trans. ASME, Vol. 105, 1983, pp. 16-22.

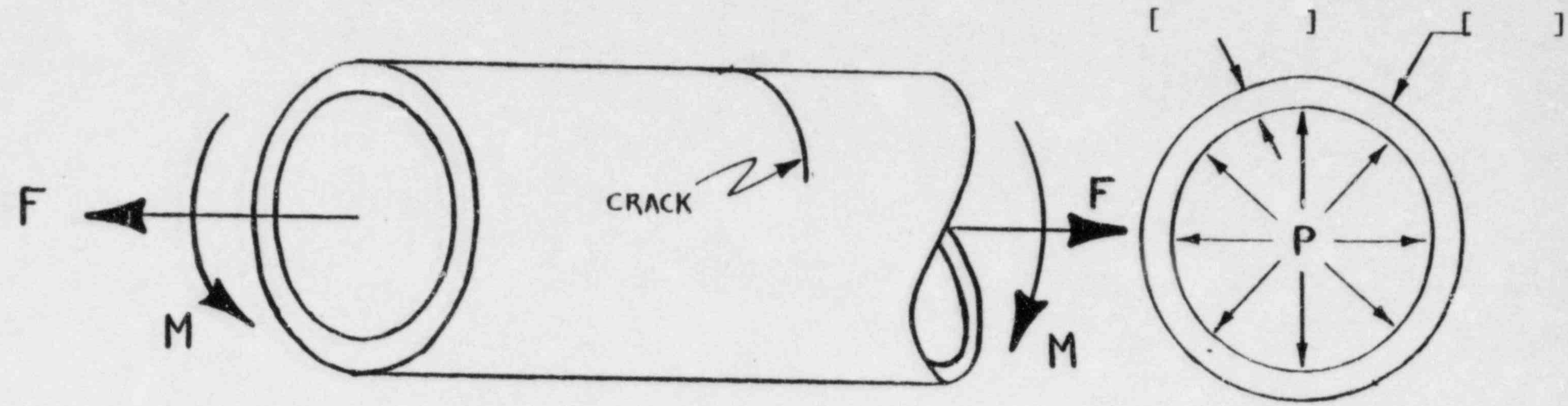
5-6 Bamford, W. H. and A. J. Bush, "Fracture Behavior of Stainless Steel," ASTM STP668, 1979, pp. 553-577.

TABLE 5-1 [

]

+a,c

Solution Step	Axial Stress* (ksi)	Bending Moment (in-kip)	J_I (in-lb/in^2)



[ ] +a,c,e

FIGURE 5-1 LOADS ACTING ON THE PIPE

+a,c,e

+a,c,e

Figure 5-2 The [ ] model. [ ] +a,c,e

+a,c,e

[ ]

Figure 5-3 The [ ] model of the pipe showing [ ] +a,c,e
[ ]

+a,c,e

+a,c,e

Figure 5-4 A close-up view of the [ ]

+a,c,e

Figure 5-5 The [ ] pattern in the vicinity of the crack front. +a,c,e

+a,c,e

Figure 5-6 [ ] on the middle of the crack surface

+a,c,e

Figure 5-7 [ ] at the pipe end which is subjected to the applied axial and bending stresses. +a,c,e

TRUE STRESS-STRAIN CURVE

Figure 5-8 True stress-strain curve and the [ ] approximation

+a,c,e

+a,c,e

Figure 5-9 Schematic of the boundary conditions.

+a,c

Figure 5-10 Loading schedule for the internal pressure applied to the inside surface of the pipe.

+a,c,e

Figure 5-11 Loading schedule for the uniform axial stress applied to the pipe end.

+a,c,e

Figure 5-12 Loading schedule for the bend moment applied to the pipe end.

+a,c,

+a,c,e

Figure 5-13

6.0 LEAK RATE PREDICTIONS

6.1 INTRODUCTION

Detailed fracture mechanics analysis has shown that through-wall cracks in the surge line would remain stable and not cause a gross failure of this RCS component. If such a through-wall crack did exist, it would be desirable to detect the leak rate such that the plant could be brought to a safe shutdown condition. The purpose of this section is to discuss the method which will be used to predict the flow through such postulated cracks and present the leak rate calculation results for a [+a,c,e

] long through wall circumferential crack using the finite element method. The mechanical stability of a slightly larger crack [ ] has been +a,c,e shown in Section 5.0 using a different [ ]. +a,c,e

6.2 GENERAL CONSIDERATIONS

The flow of hot pressurized water through an opening to a lower back pressure causes flashing which can result in choking. For long channels where the ratio of the channel length, L, to hydraulic diameter, D_H, (L/D_H) is greater than [ ], both [ ] must be considered. +a,c,e In this situation the flow can be described as being single phase through the channel until the local pressure equals the saturation pressure of the fluid. At this point, the flow begins to flash and choking occurs. Pressure losses due to momentum changes will dominate for [ ] However, for large +a,c,e L/D_H values, friction pressure drop will become important and must be considered along with the momentum losses due to flashing.

6.3 CALCULATION METHOD

The basic method used in the leak rate calculations is the method developed by [+a,c,e

]

The flow rate through a crack was calculated in the following manner. Figure 6-1 from [ ]+ was used to estimate the critical pressure, Pc, for the surge line enthalpy condition and an assumed flow. Once Pc was found for a given mass flow, the [ ] was found from Figure 6-2 of [.] For all cases considered, since [ ], [ ] Therefore, this method will yield the two-phase pressure drop due to momentum effects as illustrated in Figure 6-3. Now using the assumed flow rate G, the frictional pressure drop can be calculated using +a,c,e +a,c,e +a,c,e +a,c,e

ΔP_f = [ ] (6-1) +a,c,e

where the friction factor f is determined from the [ ] for which the crack relative roughness, ε, was obtained from fatigue crack data on stainless steel samples. The relative roughness value used in these calculations was [ ] RMS as taken from Reference [6-3]. +a,c,e +a,c,e

The frictional pressure drop using Equation (6-1) is then calculated for the assumed flow and added to the [ ] to obtain the total pressure drop from the primary system to the atmosphere. That is +a,c,e

Surge Line Pressure - 14.7 = [ ] (6-2) +a,c,e

for a given assumed flow G. If the right-hand-side of Equation (6-2) does not agree with the pressure difference between the surge line and atmosphere, then the procedure is repeated until Equation (6-2) is satisfied to within an acceptable tolerance and this then results in the flow value through the crack. This calculational procedure has been recommended by [ ] for this type of [ ] calculation. The leak rates obtained by this method have been compared in Reference [ ] with experimental results. The comparison indicated that the method predicts leak rate with acceptable accuracy [ ]. +a,c,e +a,c,e +a,c,e +a,c,e

6.4 CRACK OPENING AREAS

Figure 6-4 shows the shape of one quarter of the opened crack at the mean radius of the pipe, when the pressure and axial loadings reach their full values of [ ], respectively. Figure 6-5 in a similar plot when a moment of [ ] is superposed upon it. Table 6-1 presents the coordinates and displacements of the [ ] used to generate the two figures. The area under each curve is evaluated by numerical integration. Multiplying each of the areas by 4 gives the total areas of the cracks at the mean radius of the pipe, for the two loading conditions. Two leak rates will be calculated based on these areas. These are: +a,c,e

(a) Load A: the leak rate for the loading condition where there is [ ] +a,c,e

(b) Load B: the leak rate for the loading condition where there is [ ] +a,c,e

For load A, the crack area is:

A_A = [ ] +a,c,e

For load B, the crack area is:

A_B = [ ] +a,c,e

6.5 LEAK RATE RESULTS

Using the [FHG [6-2]] method gives [ ] leak rate for Load Case A [ ] For Load Case B, the method gives [ ] +a,c,e

Case B is considered more realistic since it [ ] Both calculated leak rates are significantly higher than the leak detection criterion of 1 gpm (Regulatory Guide 1.45). +a,c,e

6.6 <u>References</u>

[

]

TABLE 6-1 CRACK SURFACE DISPLACEMENT DATA

+a,c,e

18189-1

+a,c,e

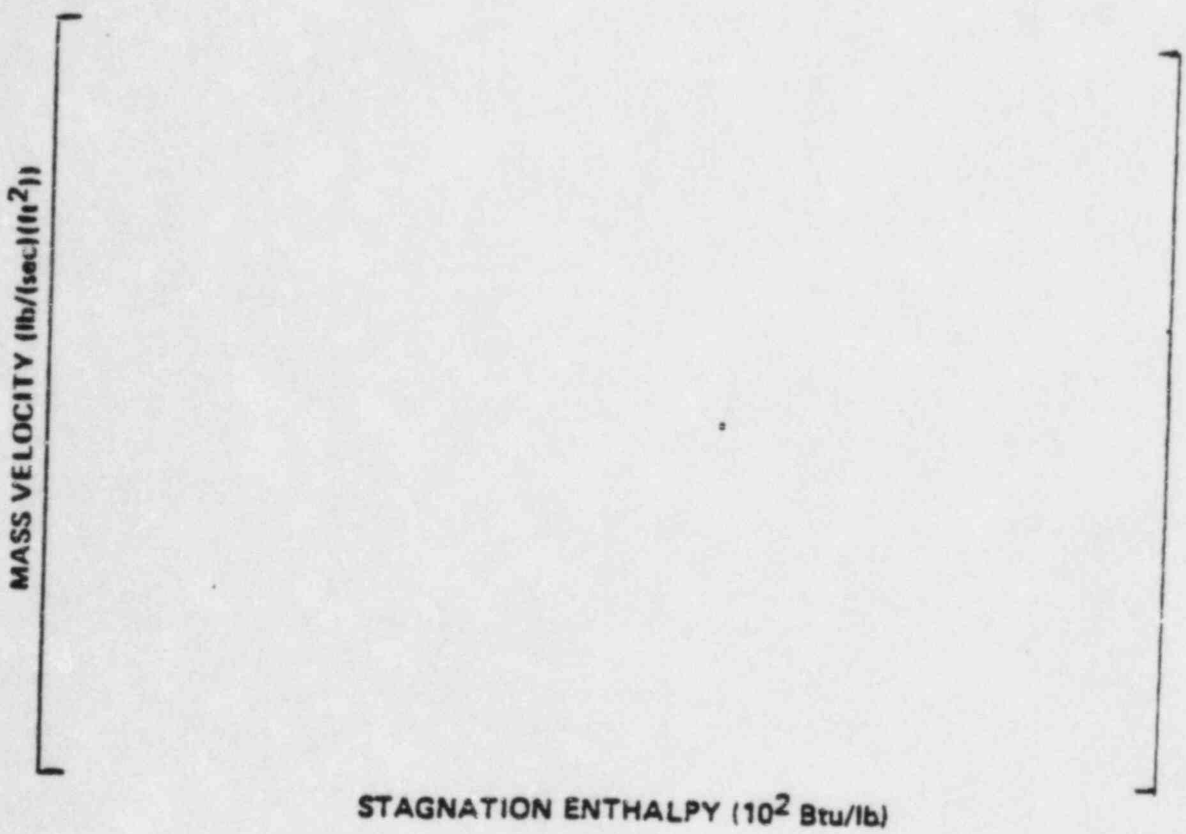


Figure 6-1 Analytical Predictions of Critical Flow Rates of Steam-Water Mixtures

+a,c,e

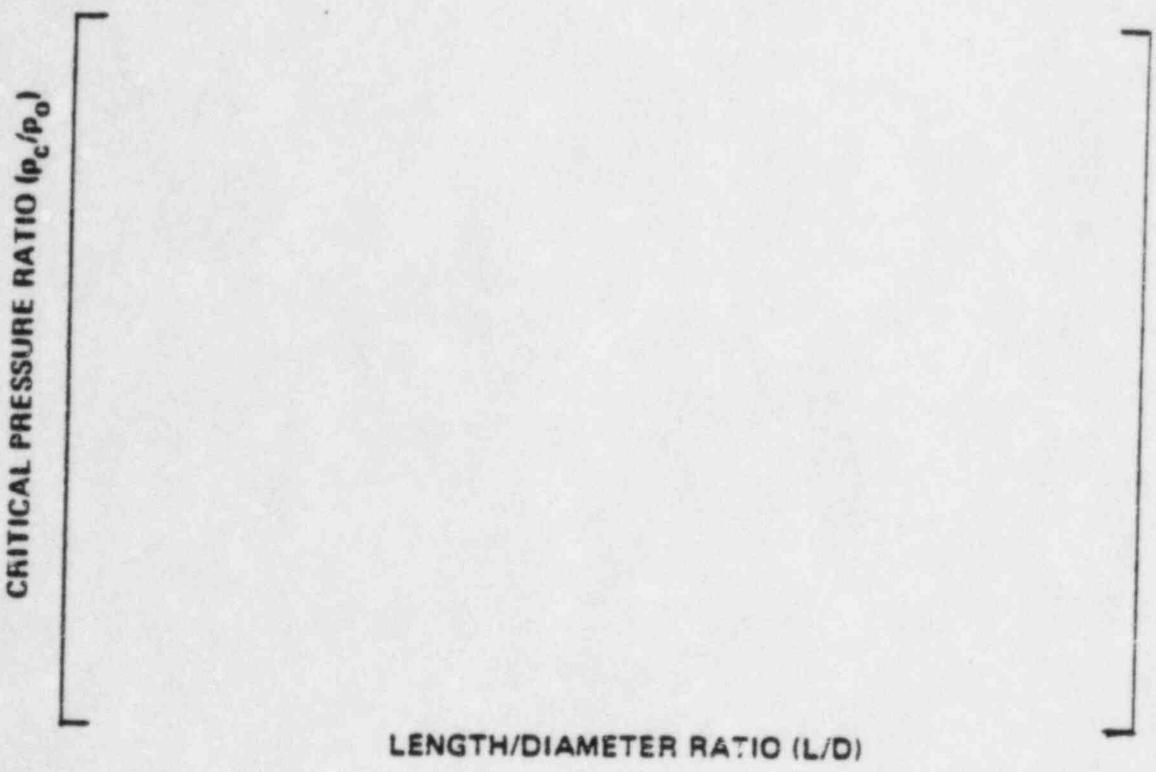


Figure 6-2 [ ] Pressure Ratio as a Function of L/D +a,c,e

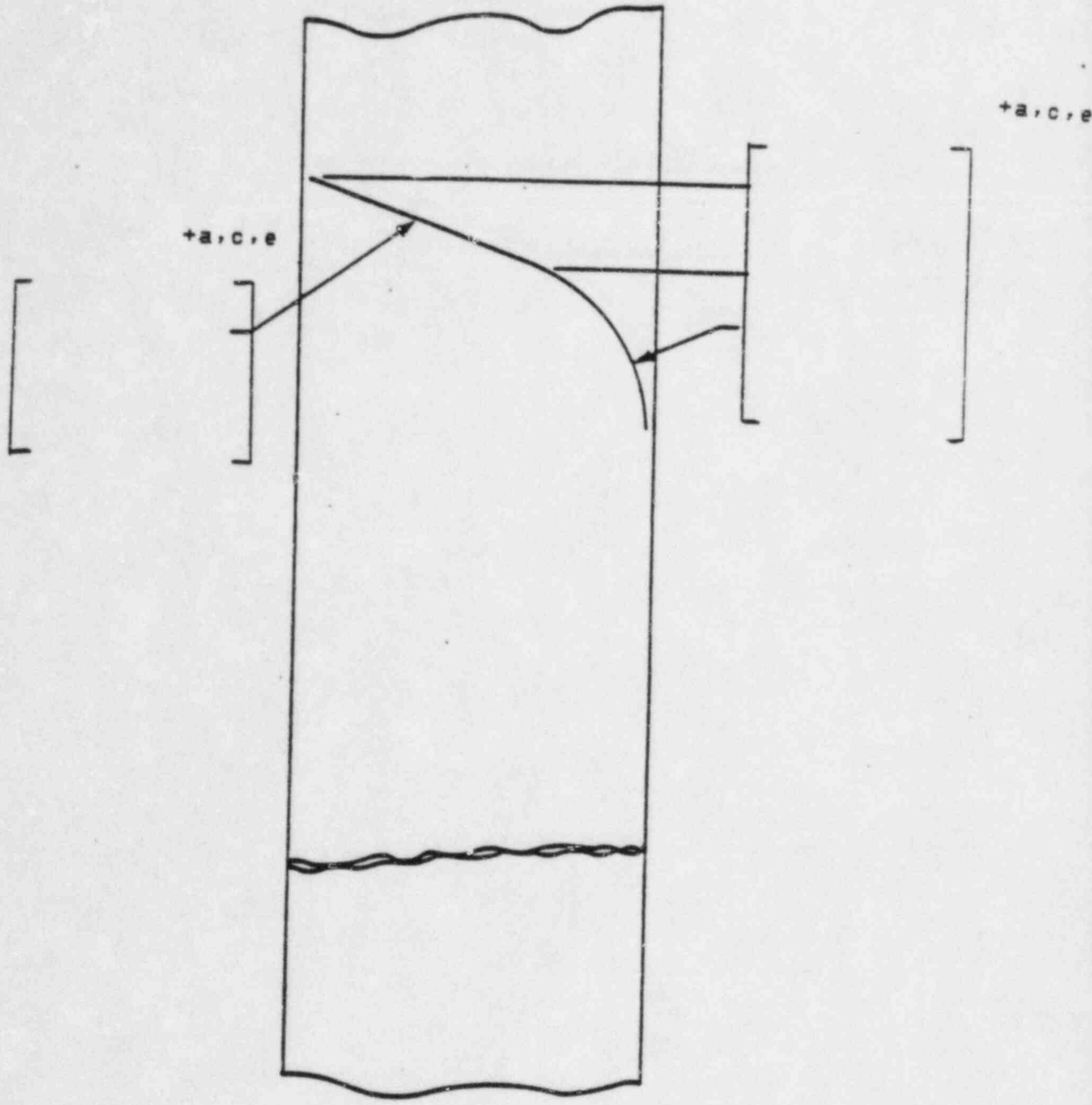


Figure 6-3 Idealized Pressure Drop Profile Through a Postulated Crack

+a,c,e

Figure 6-4 Crack surface profile under[ ] +a,c,e

+a,c,e

Figure 6-5 Crack surface profile under [ ]

+a,c,e

7.0 THERMAL TRANSIENT STRESS ANALYSIS

The thermal transient stress analysis was performed to obtain the through wall stress profiles for use in the fatigue crack growth analysis of Section 8.0. The through wall stress distribution for each transient was calculated for i) the time corresponding to the maximum inside surface stress and, ii) the time corresponding to the minimum inside surface stress. These two stress profiles are called the maximum and minimum through wall stress distribution, respectively for convenience. The constant stresses due to [ ] +a,c,e loadings were superimposed on the through wall cyclical stresses to obtain the total maximum and minimum stress profile for each transient. Linear through wall stress distributions were calculated by conservative simplified methods for all minor transients. More accurate nonlinear through wall stress distributions were developed for severe transients. These nonlinear distributions were developed from the [ ] of similar +a,c,e geometry and transients.

7.1 CRITICAL LOCATION FOR FATIGUE CRACK GROWTH ANALYSIS

The surge line stress report [3-1], design thermal transients (Section 7.2), 1-D analysis data on surge line thermal transient stresses (based on ASME Section III NB3600 rules) and the geometry were reviewed to select the worst location for the fatigue crack growth analysis. The [ ] +a,c,e was determined to be the most critical location for the fatigue crack growth evaluation. This location is selected as the worst location (same as determined in Table 3-1) based on the following considerations:

+a,c,e

i) [

ii)

iii)

iv)]

7.2 DESIGN TRANSIENTS

The transient conditions selected for this evaluation are based on conservative estimates of the magnitude and the frequency of the temperature fluctuations resulting from various operating conditions in the plant. These are representative of the conditions which are considered to occur during plant operation. The fatigue evaluation based on these transients provides confidence that the component is appropriate for its application over the design life of the plant. A total of [ ] was +a,c,e developed for the surge line by considering all the normal operating and upset transients in accordance with design specifications [7-1] and the applicable system standard design criteria documents [7-2]. Some of the data of the applicable criteria documents were refined to more closely represent the transients and to reduce the conservatism in fluid temperature fluctuations and the rate of change of fluid temperature. The thermal transients considered for the fatigue crack growth evaluation are listed in Table 7-1.

7.3 SIMPLIFIED STRESS ANALYSIS

The simplified analysis method was used to develop conservative maximum and minimum linear through wall stress distributions due to thermal transients. In this method, a 1-D computer program was used to perform the thermal analysis to determine the through wall temperature gradients as a function of time. The inside surface stress was calculated by the following equation which is similar to the transient portion of ASME Section III NB3600, Eq. 11:

+a,c,e

[

+a,c,e

] The maximum and minimum inside surface stresses were searched from the S_i values calculated for each time step of the transient solution.

The outside surface stresses corresponding to maximum and minimum inside stresses were calculated by the following equations:

+a,c,e

The following material properties at room temperature were conservatively used from the ASME Section III 1974 appendices for the pipe [ ] and nozzle [ ] safe end in the simplified calculations: +a,c,e +a,c,e

[ ] +a,c,e

The maximum and minimum linear through wall stress distribution for each thermal transient was obtained by joining the corresponding inside and outside surface stresses by [ ] The simplified analysis discussed in this section was performed for all minor thermal transients of Table 7-1 [ ] [ ] through wall stress profiles were developed for the remaining severe transients as explained in Section 7.4. The inside and outside surface stresses calculated by simplified methods for the minor transients are shown in Table 7-2. The comparison of the through wall stress profile, computed for a typical transient by the simplified method and and that based on the [ ] is shown in Figure 7-1. This figure shows that the simplified method will provide more conservative crack growth. +a,c,e +a,c,e +a,c,e

7.4 [ ] DISTRIBUTION FOR SEVERE TRANSIENTS +a,c,e

The [ ] distributions were developed from the past similar analyses for the severe transients, i.e., transient numbers [ ] As mentioned earlier, the [ ] is the worst location for fatigue crack growth analysis. A schematic of the surge line and the nozzle geometry at this location [7-3, 7-4] is illustrated in Figure 7-2. In order to develop the through wall stress distribution for the Catawba surge line, the [ ] analysis results of the pressurizer surge nozzles similar to that shown in Figure 7-2, and the results of [ ] (without discontinuity) shown in Figure 7-3, were evaluated. These results indicated that the [ ], as shown in Figure 7-2, gives the +a,c,e +a,c,e +a,c,e +a,c,e +a,c,e +a,c,e

worst through wall stress profile for the fatigue crack growth. The Catawba surge line stresses were therefore developed for this critical section which accounts for the discontinuity effects. The comparison of [ ] showed that the discontinuity at the pressurizer surge nozzle increased the [ ] by about 20 percent. It was also observed that a reduction of wall thickness by 12 percent caused the stresses to decrease by about 4 percent. These and other similar parametric evaluations from the past analyses were used to obtain appropriate factors for computing the [ ] stress profiles for the critical location of the Catawba surge line. These factors accounted for i) the effect of thickness differences, ii) the discontinuity effects and iii) the effect of differences in the fluid temperature fluctuations of the severe transients. The through wall stress distributions for the Catawba surge line were obtained by applying these factors to the finite element stress results of similar nozzle geometry. The stress profiles developed for the critical section by this method are shown in Figures 7-4, 7-5, 7-6 and 7-7 for transients [ ], respectively. The steady state fluctuation transients [ ] of Table 7-1 are identical to transient [ ] except for the maximum fluid temperature fluctuation (ΔT). Similarly, transients [ ] are identical to transient [ ] except for ΔT. Hence, the stress profiles for transient [ ] were obtained by appropriate ratioing of the stress distributions of transients [ ]

+a,c,e
+a,c,e
+a,c,e
+a,c,e
+a,c,e
+a,c,e
+a,c,e
+a,c,e

A comparison of through-wall stress profiles at the pressurizer nozzle with the profiles at a section with no discontinuities is shown in Figure 7-8 to illustrate the effect of the discontinuity.

7.5 OBE LOADS

In addition to thermal transients, cyclical stresses due to OBE event were also used for the fatigue crack growth evaluation. The maximum and minimum inside and outside OBE stresses were calculated from the OBE loading case computer run of the stress report [3-1]. A total of [ ] were considered for fatigue crack growth. The OBE stresses are as follows:

+a,c,e

	Inside Surface Stress (ksi)	Outside Surface Stress (ksi)
Maximum	[	]
Minimum		

+a,c,e

7.6 TOTAL STRESS FOR FATIGUE CRACK GROWTH

The total through wall stress at a section was obtained by superimposing the pressure load stresses and the stresses due to [+a,c,e

] Thus, the total stress for fatigue crack growth at any point is given by the following equation:

Total for Fatigue Crack Growth = [ ] +a,c,e

The thermal expansion moments were revised to account for the difference in the coefficients of thermal expansion between the Stress Report and the current ASME code for transients [ ] (which are the most severe) +a,c,e by the following equation to reduce conservatism.

M_n = [ ] (7.8) +a,c,e

where,

[ ] +a,c,e

[ ]+a,c,e

[ ] +a,c,e

7.7) for calculating the total stresses, are summarized in Table 7-3.

7.7 REFERENCES

[ ]+a,c,e

7-1

7-2

7-3

7-4

7-5

TABLE 7-1

THERMAL TRANSIENTS CONSIDERED FOR FATIGUE CRACK GROWTH EVALUATION

+a,c,e

Trans. No.	Description	No. of Occurrences	Trans. No.	Description	No. of Occurrences

TABLE 7-2

THERMAL TRANSIENT STRESSES BY SIMPLIFIED ANALYSIS

			Max Peak Stress (S_p)			Min Peak Stress (S_p)		
Transient No.	Description	Number of Occurrences	Time (sec)	Inside Max S_p (ksi)	Corresponding Outside S_p (ksi)	Time (sec)	Inside Min S_p (ksi)	Corresponding Outside Min S_p (ksi)

+a,c,e

TABLE 7-3

PRESSURE, DEADWEIGHT AND THERMAL EXPANSION STRESSES FOR FATIGUE CRACK GROWTH

Transient No.	Loading	Axial Force Fl (k)	Bending Moment BM (ft-k)	Stress (ksi)	
				Inside	Outside

[ ] +a,c,e

+a,c,e

AXIAL
STRESS
(ksi)

FIGURE 7-1: COMPARISON OF TYPICAL MAXIMUM AND MINIMUM STRESS PROFILE COMPUTED BY SIMPLIFIED [ ]

+a,c,e

FIGURE 7-2: SCHEMATIC OF SURGE LINE AT [ ] +a,c,e

+a,c,e

[

]

FIGURE 7-3: SCHEMATIC OF [ ] WITHOUT DISCONTINUITY

+a,c,e

AXIAL
STRESS
(ksi)

FIGURE 7-4: MAXIMUM AND MINIMUM STRESS PROFILE AT CRITICAL LOCATION FOR UNIT LOADING TRANSIENT

+a,c,e

AXIAL
STRESS
(ksi)

FIGURE 7-5: MAXIMUM AND MINIMUM STRESS PROFILE AT CRITICAL LOCATION FOR STEADY STATE FLUCTUATION [ ] TRANSIENT +a,c,e

+a,c,e

[

AXIAL
STRESS
(ksi)

]

FIGURE 7-6: MAXIMUM AND MINIMUM STRESS PROFILE AT CRITICAL LOCATION FOR RANDOM FLUCTUATION [ ] TRANSIENT +a,c,e

+a,c,e

AXIAL
STRESS
(ksi)

FIGURE 7-7: MAXIMUM AND MINIMUM STRESS PROFILE AT CRITICAL LOCATION FOR [ ] CYCLING TRANSIENT +a,c,e

+a,c,e

AXIAL
STRESS
(KSI)

FIGURE 7-8: EFFECT OF DISCONTINUITY ON THROUGH-WALL STRESS PROFILES

8.0 FATIGUE CRACK GROWTH ANALYSIS

The fatigue crack growth analysis was performed to determine the effect of the design thermal transients in Table 7-1 along with the OBE load transient. The analysis was performed for the critical cross section of the model which is identified in Figure 7-2. A range of crack depths was postulated, and each was subjected to the transients in Table 7-1 as well as the OBE Load transient.

8.1 ANALYSIS PROCEDURE

The fatigue crack growth analyses presented herein were conducted in the same manner as suggested by Section XI, Appendix A of the ASME Boiler and Pressure Vessel Code. The analysis procedure involves assuming an initial flaw exists at some point and predicting the growth of that flaw due to an imposed series of stress transients. The growth of a crack per loading cycle is dependent on the range of applied stress intensity factor ΔK_I, by the following relation:

$$\frac{da}{dN} = C_o \Delta K_I^{\,n} \qquad (8\text{-}1)$$

where "Co" and the exponent "n" are material properties, and ΔK_I is defined later, in Equation (8-3). For inert environments these material properties are constants, but for some water environments they are dependent on the level of mean stress present during the cycle. This can be accounted for by adjusting the value of "Co" and "n" by a function of the ratio of minimum to maximum stress for any given transient, as will be discussed later. Fatigue crack growth properties of stainless steel in a pressurized water environment have been used in the analysis.

The input required for a fatigue crack growth analysis is basically the information necessary to calculate the parameter ΔK_I, which depends on crack and structure geometry and the range of applied stresses in the area where the crack exists. Once ΔK_I is calculated, the growth due to that particular cycle can be calculated by Equation (8-1). The increment of growth is then added to the original crack size, the [illegible]ed, and the analysis proceeds to the next transient. The procedure is continued in this manner until all the transients have been analyzed.

The crack tip stress intensity factors (K_I) to be used in the crack growth analysis were calculated using an expression which applies for a semi-elliptic surface flaw in a cylindrical geometry [8-1].

The stress intensity factor expression was taken from Reference 8-1 and was calculated using the actual stress **profiles** at the critical section. The maximum and minimum stress profiles corresponding to each transient were input, and each profile was fit by a third order polynomial:

$$\sigma (x) = A_0 + A_1 \frac{x}{t} + A_2 \left(\frac{x}{t}\right)^2 + A_3 \left(\frac{x}{t}\right)^3 \qquad (8\text{-}2)$$

The stress intensity factor $K_I(\Phi)$ was calculated at the deepest point of the crack using the following expression:

+a,c,e

(8-3)

+a,c,

where

Calculation of the fatigue crack growth for each cycle was then carried out using the reference fatigue crack growth rate law determined from consideration of the available data for stainless steel in a pressurized water environment. This law allows for the effect of mean stress or R ratio ($K_{I\ min}/K_{I\ max}$) on the growth rates.

The reference crack growth law for stainless steel in a pressurized water environment was taken from a collection of data [8-2] since no code curve is available, and it is defined by the following equation:

$$\frac{da}{dN} = (0.0054 \times 10^{-3})\ (K_{eff})^{4.48} \qquad (8\text{-}4)$$

where $K_{eff} = (K_{Imax})\ (1-R)^{1/2}$

$$R = \frac{K_{I\ min}}{K_{I\ max}}$$

$\frac{da}{dN}$ = crack growth rate in micro-inches/cycle

8.2 RESULTS

Fatigue crack growth analyses were carried out for the critical cross section. Analysis was completed for a range of postulated flaw sizes oriented circumferentially, and the results are presented in Table 8-1. The postulated flaws are assumed to be six times as long as they are deep. Even for the largest postulated flaw of [ ] the result shows that flaw growth through the wall will not occur during the 40 year design life of the plant. For smaller flaws, the flaw growth is significantly lower. For example, a postulated [ ] inch deep flaw will grow to less than 1/2 the wall thickness. These results also confirm operating plant experience. There have been no leaks observed in Westinghouse PWR surge lines in over 400 reactor years of operation.

8.3 REFERENCES

8-1 McGowan, J. J. and Raymund, M., "Stress Intensity Factor Solutions for Internal Longitudinal Semi-Elliptical Surface Flaws in a Cylinder Under Arbitrary Loadings", Fracture Mechanics ASTM STP 677, 1979, pp. 365-380.

8-2 Bamford, W. H., "Fatigue Crack Growth of Stainless Steel Reactor Coolant Piping in a Pressurized Water Reactor Environment", ASME Trans. Journal of Pressure Vessel Technology, February 1979.

TABLE 8-1 FATIGUE CRACK GROWTH RESULTS

THICKNESS = [ ]* +a,c,e

INITIAL CRACK LENGTH (IN.)	CRACK LENGTH AFTER YEAR 10	20	30	40

+a,c,e

*This is conservatively taken as the minimum thickness of the counterbore region.

9.0 CONCLUSIONS

A mechanistic fracture evaluation of the Catawba Units 1 and 2 pressurizer surge line was performed. The worst location in the pressurizer surge line was identified [+a,c,e

] The critical crack length at this location was calculated as [ ]. Finite element elastic-plastic analysis was performed using a through-wall flaw [+a,c,e +a,c,e

] The applied [ ] +a,c,e was calculated corresponding to the maximum applied load including the Safe Shutdown Earthquake load. The applied [ ] is thus less than +a,c, [ ] for the material. These results demonstrate +a,c, that [ ] crack will remain stable when subjected to maximum loading +a,c, conditions considering both global and local failure mechanisms.

The leakage through a crack [ ] long [ ] was calculated as [ ] +a,c gpm under the normal operating loads. The Catawba Plant has an RCS pressure boundary leak detection system which is consistent with the requirements of Regulatory Guide 1.45 and can detect leakage of 1 gpm in one hour. Thus, there is a factor of at least [ ] between the calculated leak rate and the Catawba +a, plant leak detection systems capability.

Fatigue crack growth was determined for postulated inside surface flaws using plant design transients. Crack growth results indicated that even a postulated surface flaw which is [ ] of the wall thickness in depth will not penetrate +a,c the wall over the plant life. Thus, there is no known mechanism which could cause a through-wall crack of the type assumed in the stability calculations.

The incidence of stress corrosion cracking is eliminated by appropriate water chemistry control. Furthermore, operational occurrences will not create water hammer in the surge line.

Based on the above, it is concluded that guillotine breaks in the pressurizer surge line should not be considered as a part of the structural design basis of the Catawba plant.

APPENDIX A

EQUILIBRIUM OF THE SECTION

APPENDIX A

The internal stress system at the crack plane has to be in equilibrium with the applied loading i.e. the hydrostatic pressure P, axial forc: F and the bending moment M_b. The angle β which identifies the point of stress inversion follows from the equilibrium of horizontal forces (See Figure A-1). This is

[ ] +a,c,e

Solving for β,

[ ] +a,c,e

The external bending moment at the instant of failure follows from the equilibrium of moments, which is most easily taken around the axis 1-1. Thus M_b can be determined from

[ ] +a,c,e

+a,c,

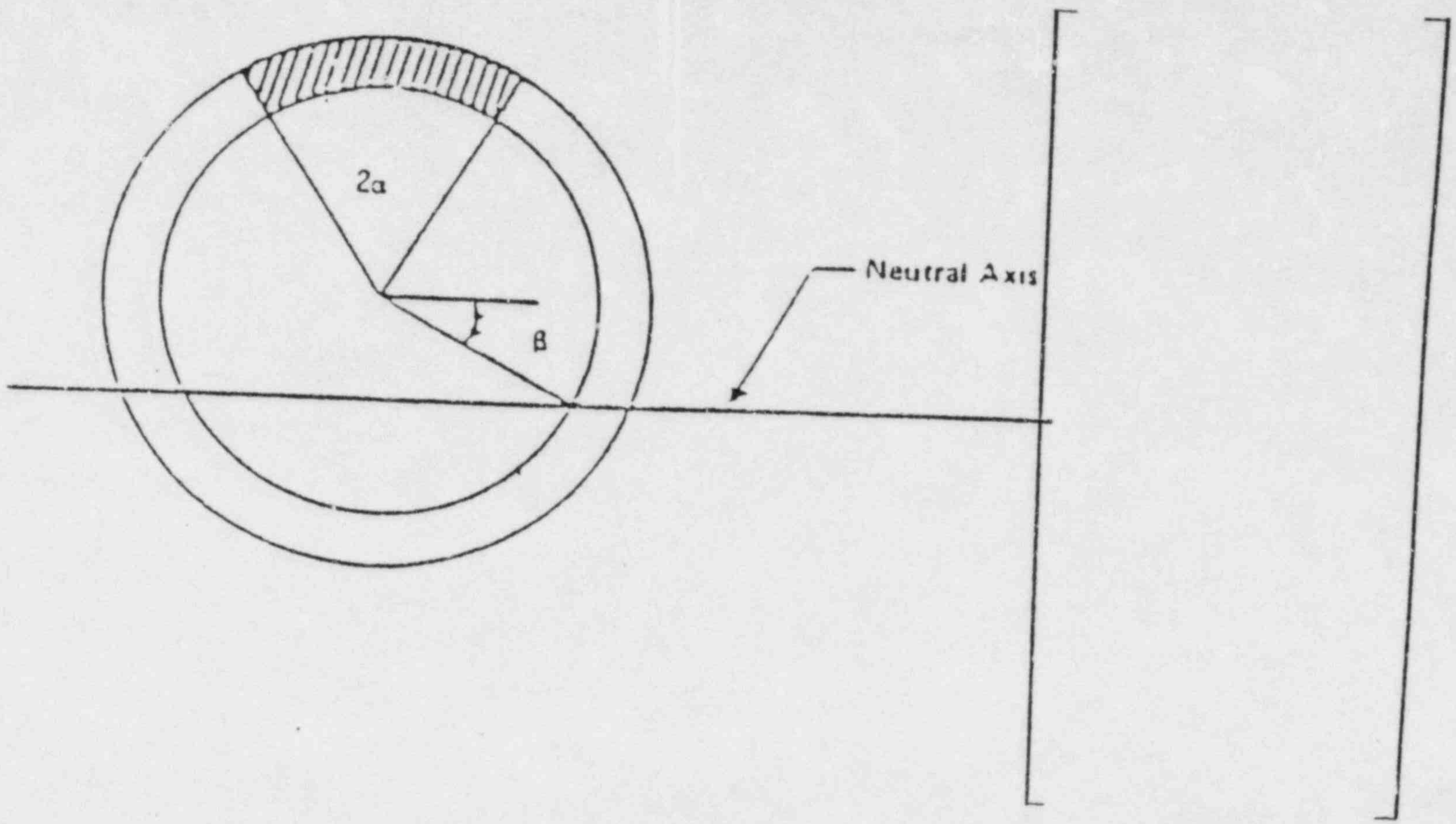


Figure A-1 Equilibrium of Horizontal Forces

APPENDIX B

VERIFICATION OF THE FINITE ELEMENT RESULTS

The purpose of the verification presented herein is to assure the correctness of the fracture mechanics analysis for the pipe. Both the K_I-values due to the pure axial stretching and the pure bending are investigated. The outer fiber stresses corresponding to the maximum applied bending moment are investigated also.

(1) <u>The K_I for a circumferentially cracked pipe subjected to pure bending</u>.

The elastic solution for this problem has been studied by Folias [B-1] and others. Under the present geometrical and loading conditions, the K_I is given by

+a,c,e

[ ]

[+a,c,e

]

Substituting [ ] +a,c,e ksi$\sqrt{in}$. The difference between the results by Eq. (B-3) and the VCE method is 4.5 percent. Better agreement would have been obtained if the bending effect, i.e., the ε factor, had been included in the calculation. However, the 4.5 percent errror is acceptable for the engineering analyses.

(2) K_I due to pure bending

The K_I for a circumferentially cracked pipe subjected to bending may be estimated by taking the average of that produced by the tensile outer fiber stress, σ_b, and by the fiber stress at the location of the crack tip, σ'. These stresses are shown in Figure B-1. The relation between σ_b and σ' is given by

$$\sigma' = \sigma_b \cos \alpha \qquad \text{(B-5)}$$

where α=crack angle (see Figure B-1). Therefore the K_I due to bending is

$$K_{I,b} = [\qquad] \qquad \text{(B-6)}$$ +a,c,e

Inserting Eq. B-5 into Eq. B-6 and taking [

], one obtains:

$$K_{I,b} = [\qquad] \qquad \text{(B-7)}$$ +a,c,

A pure bending load with [

] was +a,c,

used for the VCE analysis and the [ ] produced [+a,c,

] This [ ] is converted to the [ ] of +a,c,

[ ] +a,c,

Substituting [+a,c,e

] The difference in this case is about 3 percent.

It need be noted that Eqs. B-3 and B-7 are valid only for the elastic deformation. When loads increase the linear elastic theory underestimates the [ ] The deviation is considerable when +a,c,e large plastic zone in the crack tip region is developed. However, these equations can be used for reference purpose. This means that the actual [ ] should be always greater than those given by Eqs. B-1 and +a,c,e B-6. This condition or requirement is met for the present analysis.

(3) Check on the Outer fiber stress

In addition to examining the [ ] values, the axial stress +a,c,e which directly relates to the open mode of fracture is examined herein. Only the outer fiber stress on the tension side is checked. Since there is no plastic deformation in the region remote from the crack up to the maximum applied loads, the bending stress can be computed by

$$\sigma_b = \frac{M}{I} z \tag{B-8}$$

where M = bending moment

I = moment of initia

z = distance from the neutral axis.

Based on the geometrical data employed in the present analysis, [+a,c,

] For [+a,c,

] In addition to

the bending stress, there is an axial stress, σ_a, of [ ] ksi +a,c,e
constantly acting on the pipe. Therefore, the combined fiber stress at the Guassian point investigated is

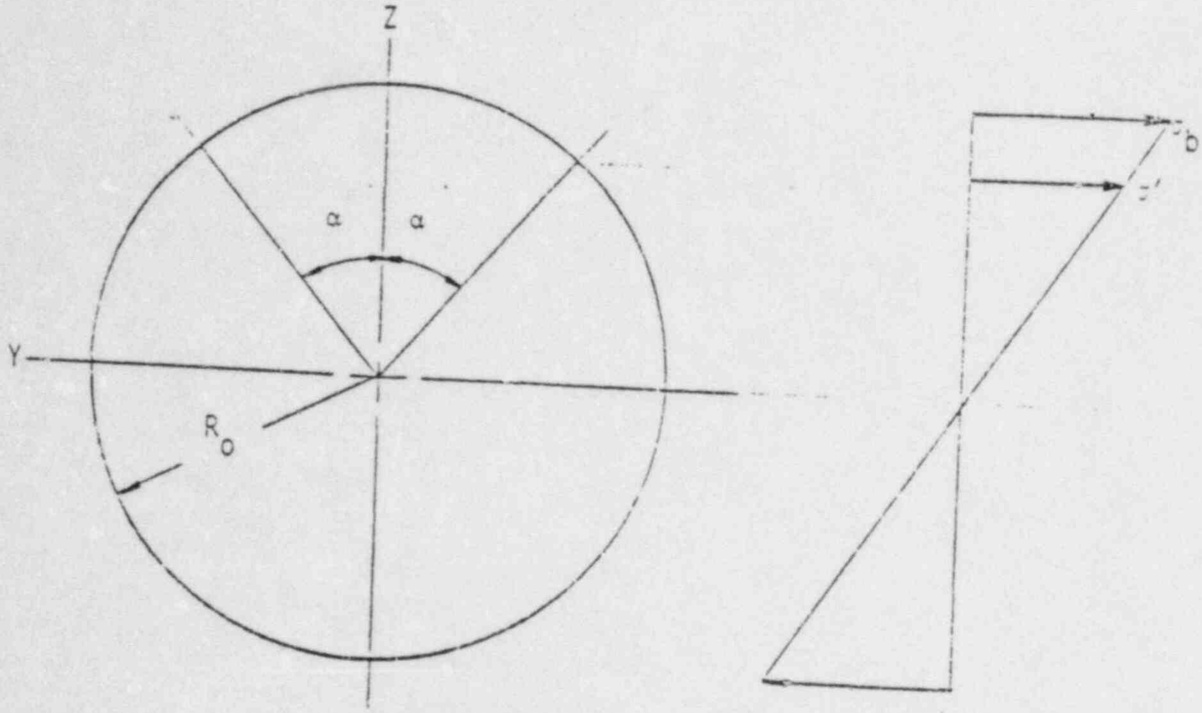
$$\sigma_{tot} = \sigma_a + \sigma_b$$

$$= [\quad]$$ +a,c,e

The corresponding stress given by [ ] is [ ] ksi. The error is +a,c,e
0.4 percent.

Reference

B-1 Folias, E. S., "On the Effect of Initial Curvature on Cracked Flat Sheets," Int. J. of Frac. Mech., Vol. 5, 1969, pp. 327-346.



$$\sigma_b = \frac{M}{I} R_o$$

$$\sigma' = \frac{M}{I} (R_o \cos\alpha) = \sigma_b \cos\alpha$$

α = crack angle

Figure B-1 Auxiliary diagram for derivation of Equation 3-6.

CNS

3.6 PROTECTION AGAINST DYNAMIC EFFECTS ASSOCIATED WITH THE POSTULATED RUPTURE OF PIPING

General Design Criterion 4 of Appendix A to 10CFR50 required that structures, systems, and components important to safety be protected from the dynamic effects of pipe failure. This section describes the design bases and design measures to ensure that the containment vessel and all essential equipment inside or outside the containment, including components of the reactor coolant pressure boundary, have been adequately protected against the effects of blowdown jet and reactive forces and pipe whip resulting from postulated rupture of piping.

Except where specified otherwise in Table 3.6.1-3, criteria presented herein regarding break size, shape, orientation, and loca tion are in accordance with the guidelines established by NRC Regulatory Guide 1.46, and include considerations which are further clarified in NRC Branch Technical Position MEB 3-1 and APCSB 3-1 where appropriate. These criteria are intended to be conservative and allow a high margin of safety. For those pipe failures where portions of these criteria lead to unacceptable consequences, further analyses will be performed. However, any alternative criteria will be adequately justified and fully documented.

3.6.1 POSTULATED PIPING FAILURES IN FLUID SYSTEMS INSIDE AND OUTSIDE CONTAINMENT

3.6.1.1 Design Bases

3.6.1.1.1 Reactor Coolant System

The Reactor Coolant System, as used in Section 3.6 of the Safety Analysis Report, is limited to the main coolant loop piping and all branch connection nozzles out to the first butt weld. Dynamic effects are only considered for pipe breaks postulated at branch connections. The particular arrangement of the Reactor Coolant System, building structures, and mechanical restraints preclude the formation of plastic hinges for breaks postulated to occur at the branch connections. Consequently, pipe whip and jet impingement effects of the postulated pipe break at these locations will not result in unacceptable consequences to essential components. This restraint configuration, along with the particular arrangement of the Reactor Coolant System and building structures, mitigates the effects of the jet from the given break such that no unacceptable consequences to essential components are experienced.

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The application of criteria for protection against the effects of postulated breaks at the branch connections results in a system response which can be accommodated directly by the supporting structures of the reactor vessel, the steam generator, and the reactor coolant pumps. The design bases for postulated breaks in the Reactor Coolant System are discussed in Section 3.6.2.1.

Systems which do not contain mechanical pressurization equipment are excluded from moderate-energy classification (e.g., systems without pumps, pressurizing tanks, boilers, or those which operate only from gravity flow or storage tank water head), however, limited failures are assumed to occur for the purpose of considering the effects of flooding, spray, and wetting of equipment in the station analysis.

The identification of piping failure locations will be performed in accordance with Section 3.6.2.

3.6.1.1.2.1 Interaction Criteria

The following criteria define how interactions shall be evaluated. The safety evaluation of each interaction is described in Sections 3.6.1.3 and 3.6.1.1.5.

a) Environmental Interaction

An active component (electrical, mechanical, and instrumentation and control) is assumed incapable of performing its function upon experiencing environmental conditions exceeding any of its environmental ratings.

b) Jet Impingement Interactions

Active components (electrical, mechanical, and instrumentation and control) subjected to a jet are assumed failed unless the active component is enclosed in a qualified enclosure, the component is known to be insensitive to such an environment, or unless shown by analysis that the active function will not be impaired.

c) Pipe Whip Interaction

A whipping pipe is not to be considered to inflict unacceptable damage to other pipes of equal or greater size and wall thickness.

A whipping pipe is only considered capable of developing through-wall leakage cracks in other pipes of equal or greater size with smaller wall thickness.

An active component (electrical, mechanical, and instrumentation and control) is assumed incapable of performing its active function following impact by any whipping pipe unless an analysis or test is conducted to show otherwise.

3.6.1.1.3 Protective Measures

3.6.1.1.3.1 Reactor Coolant System

The fluid discharged from postulated pipe breaks at branch connections will produce reaction and thrust forces in branch line piping. The effects of these

c) Safety-related portions of the Main Steam and Feedwater Systems are Duke Class B. Class B system materials, fabrication, nondestructive examinations and documentation are in accordance with ASME Section III, Class 2.

d) The guard pipe on the main steam piping inside containment is extended over the length of the vertical portion of the piping to prevent jet impingement on the ice condenser doors following a postulated rupture.

e) As a result of a Duke-NRC meeting in May 1976, guard pipe was removed from the main steam and main feedwater piping in the doghouse which was originally designed with guard pipe. Breaks in the Main Steam piping will be postulated based on consequence except for the break exclusion region inside the doghouse. For the main steam piping which is part of the break exclusion region an augmented inservice inspection will be performed, as discussed in Section 6.6, for welds where no break is selected. Breaks in the main feedwater will be selected as outlined in Section 3.6.2.

3.6.1.1.3.4 Control Room Protection from Postulated Piping Breaks

The control room is located on the top floor of the Auxiliary Building and is bounded on the north and south sides by electrical penetration rooms which contain no piping. The east side of the control room is bounded by the equipment area housing the control room ventilation equipment. Piping in this area consists of low pressure, low volume chilled water and low pressure, low volume heating steam. On the west side, the control room is bounded by the computer room and supporting areas. Piping in this area consists of sanitary waste and vent piping, drinking water and instrument air; none of which are high-energy systems. Immediately below the control room is the cable room containing no piping.

Based on the above physical parameters, the control room is structurally isolated from areas containing high-energy systems; therefore, there are no unacceptable consequences to the control room from the postulated break of high-energy piping systems.

3.6.1.1.4 Acceptability Criteria

The capability to eventually achieve a cold shutdown condition is not jeopardized even if the pipe failure is followed by a single active failure. The system requirements and available redundancy are determined on a shutdown logic diagram, or a required equipment list for mitigating the effects of the postulated failure.

Repair of failures is considered to assure achievement of the cold shutdown condition where such repairs can be shown to be practical and timely, and provided the unit can be held in a safe shutdown state during the time required for the repair.

3.6.1.1.5 Leak-Before-Break Analyses for High Energy Piping Other Than Reactor Coolant System

Developments in fracture mechanics make it possible to prove applicability of a "leak before break" pipe failure concept for certain piping systems in addition to the Reactor Coolant System. Under this concept the size flaw

which results in detectable leakage is shown to be much smaller than the size flaw which would lead to a catastrophic double-ended pipe break (DEPB). It is shown that there is no mechanism for developing a large break without going through an extended period during which the crack would leak copiously and corrective action would be taken in accordance with plant Technical Specifications.

The "leak before break" analysis involves, in general, the following steps:

1. Compilation of data on piping loads, materials, geometry, and transients.

2. Determining piping stresses and initial flaw sizes and locations.

3. Performing fatigue crack growth calculations using fracture mechanics techniques.

4. Evaluating crack stability and calculating leakage rate from a stable crack.

5. Comparing leak rate predictions with leak detection capability.

If the leakage rate from a stable crack is shown to be within the limits of leak detectability, then the applicability of the "leak before break" concept has been demonstrated.

For high energy piping where applicability of the "leak before break" concept has been demonstrated, the postulation of DEPB's per Sections 3.6.2.1.2.1 and 3.6.2.1.2.3 is replaced for certain consequence analyses by postulation of cracks. The crack size and orientation is defined by the detailed fracture mechanics analysis. The dynamic effects of pipe rupture described in Sections 3.6.2.2 and 3.6.2.3 are not applicable. Compartment pressurization effects are limited to consideration of pressurization due to the crack. This elimination of DEPB also eliminated the need to perform the consequence analysis for pipe whip, jet impingement, and compartment pressurization associated with these DEPB's. For flooding analyses and environmental effects (temperature, humidity, and water spray), the DEPB is retained and Sections 3.6.2.1.2.1 and 3.6.2.1.2.3 apply. Table 3.6.1-4 lists the lines for which "leak before break" concept applicability is demonstrated.

3.6.1.2 Description of Piping System Arrangement

Separation is the primary consideration in piping system layout and arrangement. Where physical separation is not feasible, protective devices are provided to protect essential components.

3.6.2.1.1.1 Postulated Piping Break Locations and Orientations

MEB Q13 Reference 1 defines the original basis for postulating pipe breaks in the reactor coolant system primary loop. Reference 1.a provides the basis for eliminating from certain aspects of design consideration previously postulated reactor coolant system pipe breaks, with the exception of those breaks at branch connections. See Table 3.6.2-1 and Figure 3.6.2-2.

3.6.2.1.1.2 Postulated Piping Break Sizes

For a circumferential break, the break area is the cross-sectional area of the pipe at the break location, unless pipe displacement is shown to be limited by analysis, experiment or physical restraint.

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3.6.2.1.1.3 Line Size Considerations for Postulated Piping Breaks

Branch lines connected to the Reactor Coolant System are defined as "large" for the purpose of this criteria as having an inside diameter greater than 4 inches up to the largest connecting line. Where postulated, pipe break of these lines results in a rapid blowdown of the Reactor Coolant System and protection is basically provided by the accumulators and the low head safety injection pumps (residual heat removal pumps).

3.6.2.1.2 General Design Criteria for Postulated Piping Breaks Other Than Reactor Coolant System

a) Station design considers and accommodates the effects of postulated pipe breaks with respect to pipe whip, jet impingement and resulting reactive forces for piping both inside and outside Containment except as specified in section 3.6.1.1.5. The analytical methods utilized to assure that concurrent single active component failure and pipe break effects do not jeopardize the safe shutdown of the reactor are outlined in Section 3.6.2.3.

b) Station general arrangement and layout design of high-energy systems utilize the possible combination of physical separation, pipe bends, pipe whip restraints and encased or jacketed piping for the most practical design of the station. These possible design combinations decrease postulated piping break consequences to minimum and acceptable levels. In all cases, the design is of a nature to mitigate the consequences of the break so that the reactor can be shutdown safely and eventually maintained in a cold shutdown condition.

c) The environmental effects of pressure, temperature and flooding are controlled to acceptable levels utilizing restraints, level alarms and/or other warning devices, and vent openings.

p) Minimum essential component and systems performance is provided as required for the type of break.

q) The effects of pipe ruptures are not allowed to result in offsite doses in excess of 10CFR100 allowable limits.

r) Operability in an environment is assured for all equipment required to mitigate the break by the equipment specification requirements based on conservative design conditions.

s) Emergency procedures are prepared that are to be followed after a postulated piping break for high-energy systems as required.

3.6.2.1.2.1 Postulated Piping Break Locations For High-Energy Piping Systems

Systems identified as containing high-energy piping are examined by detailed design drawing review for postulated pipe breaks as defined below. Systems analyzed for consequences of postulated piping breaks are listed in Table 3.6.1-1. Refer to Section 3.6.1.1.5 for exceptions to the following criteria due to application of the "Leak Before Break" concept.

Terminal ends are considered as piping originating at structures or components (such as vessel and equipment nozzles and structural piping anchors) that act as rigid constraint to the piping thermal expansion. Typically, the anchors assumed for the piping code stress analysis would be terminal ends. The branch connection to the main run is one of the terminal ends of a branch run, except intersections of runs of comparable size and fixity which have a significant effect on the main run need not be considered terminal ends when the stress analysis model includes both the run and branch piping and the intersection is not rigidly constrained to the building structure.

a) Breaks in Duke Class A piping are postulated at the following locations (see Table 3.2.2-3 for class correlations):

   1) The terminal ends of the pressurized portions of the run.

   2) At intermediate locations selected by either one of the following methods:

      i) At each weld location of potential high stress or fatigue, such as pipe fittings (elbows, tees, reducers, etc.), valves, flanges, and welded attachments, or

      ii) At all intermediate locations between terminal ends where the following stress and fatigue limits are exceeded,

         a. The maximum stress range shall not exceed 2.4 S_m except as noted below.

3.6.2.1.3 Failure Consequences

The interactions that are evaluated to determine the failure consequences associated with postulated pipe breaks are dependent on the energy level of the contained fluid. They are as follows:

a) High-Energy Piping

MEB Q23
1) Circumferential Breaks and Longitudinal Splits
   a) Pipe Whip (displacement)
   b) Jet Impingement
   c) Compartment Pressurization
   d) Flooding
   e) Environmental Effects (Temperature, humidity, water spray)

2) Througnwall leakage cracks
   a) Environmental Effects (Temperature, Humidity)
   b) Flooding

1b) Moderate-Energy Piping

MEB Q23
1) Through-wall leakage cracks
   a) Flooding
   b) Environmental Effects (Temperature, humidity, water spray)
   c) Water Spray

For high energy piping there are certain exceptions as detailed in Reference 1a for the reactor coolant loop and Section 3.6.1.1.5 for all other piping systems.

3.6.2.2 Analytical Methods to Define Forcing Functions and Response Models

3.6.2.2.1 Reactor Coolant System Dynamic Analysis

This section summarizes the dynamic analysis as it applies to the LOCA resulting from the postulated design basis pipe breaks at main reactor coolant branch line connections.Further discussion of the dynamic analysis methods used to verify the design adequacy of the reactor coolant loop piping, equipment and supports is given in Reference 1 as it pertains to postulated breaks at branch connections.

The particular arrangement of the Reactor Coolant System for the Catawba Nuclear Station is accurately modeled by the standard layout used in Reference 1 and the postulated branch connection break locations do not change from those presented in Reference 1.

In addition, an analysis is performed to demonstrate that at each postulated branch connection break location the motion of the pipe ends is limited so as to preclude unacceptable damage due to the effects of pipe whip or large motion of any major components. The loads employed in the analysis are based on full pipe area discharge except where limited by major structures.

to this closed valve.

ting at structure or components that act as rigid constraint to the piping thermal expansion. Typically, the anchors assumed for the code stress analysis would be terminal ends. Stresses in the system either side of the closed valve will be about the same; therefore, terminal end classification based on constraint and high stresses are not applicable. Duke reviews these closed valve locations to assure high stresses are not developed as a result of rigid constraint from nearby anchors of component connections in the non-pressurized portion of the piping.

APCSB 3-1, Appendix B and C

In Appendix B, pipe break locations are specified for ASME Section III Code Class, 1, 2, and 3 piping such that a minimum of two intermediate breaks are selected per run although threshold limits are not exceeded (for ASME Section III Code Class 1, 2, and 3 piping). In Appendix C, a minimum of either two or one intermediate breaks within the boundary of each compartment is specified.

SAR Section 3.6.2.1.2.1

Duke criteria specifies that if the threshold stress levels are not exceeded, then no intermediate breaks are postulated.

APCSB 3-1, Appendix B

The criteria used to determine the design basis piping break locations involves the assumption of double-ended pipe break locations selected according to stress and/or fatigue analyses, or consequence analysis.

SAR Section 3.6.1.1.5

In specified cases the "leak before break" concept is employed, as explained in Section 3.6.1.1.5.

MEB 3-1, Section B.1

The criteria used to determine the design basis piping break locations involves the assumption of double-ended pipe break locations selected according to stress and/or fatigue analyses, or consequence analysis.

SAR Section 3.6.1.1.5

In specified cases the "leak before break" concept is employed, as explained in Section 3.6.1.1.5.

MEB 3-1, Section B.1.b(6)

Section B.1.b(6) requires that guard pipe assemblies between containment isolation valves meet the following requirements:

a. The design pressure and temperature should not be less than the maximum operating temperature and pressure of the enclosed pipe under normal plant conditions.

b. The design stress limits of Paragraph NE-3131(c) should not be exceeded under the loading associated with design pressure and temperature in combination with the safe shutdown earthquakes.

c. Guard pipe assemblies should be subjected to a single pressure test at a pressure equal to design pressure.

SAR Section 3.6.2.4

Duke criteria is different from NRC criteria as described and justified below:

Guard pipes provided between containment isolation valves are designed in accordance with SAR Section 3.6.2.4. Guard pipes are subjected to a pressure test as required by the material specification before welding to the penetration assembly.

It is impractical to test guard pipes in the finished penetration assembly due to the configuration and potential damage to internal process pipe and associated insulation. Independent design analysis have been conducted to provide assurance that Duke penetration designs are acceptable. In addition, the extent of NDT conducted on guard pipes to flued head butt weld is such to assure integrity of design.

MEB Q35

MEB 3-1, Section B.1.c(1)

Intermediate breaks in Class 1 piping are postulated at the two highest stress locations based on Equation (10) if two intermediate locations cannot be determined by application of Equations (10), (12), and (13) or U>0.1.

SAR Section 3.6.2.1.2.1.

Duke criteria states that if there are no intermediate locations where S exceeds 2.4 S_m or U exceeds 0.1, no intermediate breaks are postulated.

MEB 3-1, Section B.1.c(2)

Intermediate breaks in Class 2 and 3 piping are postulated where the stresses exceed 0.8 ($1.2S_h$ + S_A) but at not less than two locations based on highest stress. Where the piping consists of a straight run without fittings, welded attachments, and valves, and all stresses are less than 0.8 ($1.2S_h$ + S_A), a minimum of one location should be chosen based on highest stress.

SAR Section 3.6.2.1.2.1

Duke criteria specifies that if the threshold stress levels are not exceeded, then no intermediate breaks are postulated.

MEB Q35

MEB 3-1, Sections B.1.c(3)

Breaks in non-nuclear piping should be postulated at the following location:

a. Terminal ends,

b. At each intermediate pipe fitting, welded attachment, and valve.

SAR Section 3.6.2.1.2.1

Duke criteria is generally equivalent to NRC criteria as described and justified below:

Breaks in Duke Class F piping (non-nuclear, seismic) are postulated at terminal ends and at intermediate locations based on the use of ASME Section III analysis techniques, the same as Duke Class B and C piping. Duke Class F piping is constructed in accordance with ANSI B31.1 and is dynamically analyzed and restrained for seismic loadings similar to ASME Section III piping. Materials are specified, procured, received, stored, and issued under Duke's QA program similar to ASME Section III materials except that certificate of compliance in lieu of mill test reports are acceptable on minor components, and construction documentation for erected materials is not uniquely maintained. Construction documentation for erected materials is generically maintained. MTR are required for the bulk of piping materials.

MEB 3-1, Section B.2.e

Through-wall cracks may be postulated instead of breaks in those fluid systems that qualify as high energy fluid systems for short operational periods. This operational period is defined as about 2 percent of the time that the system operates as a moderate energy fluid system.

SAR Section 3.6.1.1.2

Duke criteria is generally equivalent to NRC criteria as clarified below:

The operational period that classifies such systems as moderate energy is either:

a. One percent of the normal operating lifespan of the plant, or

b. Two percent of the time period required to accomplish the system design function.

Regulatory Guide 1.46

Longitudinal breaks are postulated in piping runs 4 inches nominal pipe size and larger. Circumferential breaks are postulated in piping runs exceeding 1 inch nominal pipe size.

As a minimum, there should be two intermediate break locations for each piping run or branch run.

SAR Section 3.6.2.1.2.1

Duke criteria is the same as NRC Branch Technical Position APCSB 3-1 and roughly equivalent to Regulatory Guide 1.46 with expansion of definition as described below:

Longitudinal breaks are postulated in piping runs 4 inches nominal pipe size and larger except that longitudinal breaks are not postulated at terminal ends where the piping has no longitudinal welds.

Duke criteria specifies that if the threshold stress levels are not exceeded, then no intermediate breaks are postulated.

Regulatory Guide 1.46

A whipping pipe should be considered capable of rupturing an impacted pipe of smaller nominal pipe size and lighter wall thickness.

SAR Section 3.6.2.1.2

Duke criteria is the same as NRC Branch Technical Position APCSB 3-1 and roughly equivalent to Regulatory Guide 1.46 with expansion of definition as described below:

The energy associated with a whipping pipe is considered capable of (a) rupturing impacted pipes of smaller nominal pipe sizes, and (b) developing through-wall cracks in larger nominal pipe sizes with thinner wall thicknesses.

Regulatory Guide 1.46

Measures for restraint against pipe whipping as a result of the design basis breaks postulated... need not be provided for piping where ... the following applies:

Both of the following piping system conditions are met:

(1) the design temperature is 200°F or less, and

(2) the design pressure is 275 psig or less.

SAR Section 3.6.1.1.2

Duke criteria is generally equivalent to Regulatory Guide 1.46 with expansion of definition as described below:

High energy piping is reviewed for pipe whipping and is defined as those systems that during normal plant conditions are either in operation of maintained pressurized under conditions where either or both of the following are met:

a. maximum temperature exceeds 200°F, or

b. maximum pressure exceeds 275 psig, except that (1) non-liquid piping system with a maximum pressure less than or equal to 275 psig are not considered high energy regardless of the temperature, and (2) for liquid systems other than water, the atmospheric boiling temperature can be applied.

Systems are classified as moderate energy if the total time that either of the above conditions are met is less than either:

a. one (1) percent of the normal operating lifespan of the plant, or

b. two (2) percent of the time period required to accomplish its system design function

Regulatory Guide 1.46	SAR Section 3.6.1.1.5
The criteria used to determine the design basis piping break locations involves the assumption of double-ended pipe break locations selected according to stress and/or fatigue analysis, or consequence analysis.	In specified cases the "leak before break" concept is employed, as explained in Section 3.6.1.1.5.

Table 3.6.1-4

Piping Systems For Application of "Leak Before Break" Concept

System	Math Model	Line Description
Reactor Coolant	--	Reactor Coolant Loops
Reactor Coolant	NC-201	Pressurizer Surge Line

A typical piping analysis problem, with representative math model, is shown in Figure 3.9.3-1. The results of this analysis are given in Table 3.9.3-10.

3.9.3.1.4 Design Loading Combinations and Design Stress Limits for Mechanical Equipment Furnished by Duke

The load combinations and corresponding stress criteria for Duke Mechanical equipment and valves are presented in Table 3.9.3-9.

3.9.3.1.5 Piping Supports and Restraints

The design loading combination associated with each component operating condition is given in Table 3.9.3-11 for supports, restraints, and anchors and in Table 3.9.3-12 for mechanical or hydraulic snubbers.

Loads for each loading combination are combined algebraically except that components which contain positive and negative values are combined to assemble the worst case load combination.

Design stress limits for each component operating condition are in accordance with Subsection NF of the ASME Boiler and Pressure Vessel Code for those portions of supports and restraints within the NF jurisdictional boundary. Stress limits for Normal and Upset Conditions are in accordance with Article XVII-2000. For Faulted Condition, design stress limits for manufacturer's standard support components are in accordance with the requirements of Appendix F. Emergency Condition stress limits, as specified in Article XVII-2000 are used for the design of all other components for Faulted Condition. Stresses for those portions of supports and restraints outside the NF jurisdictional boundary are limited to the allowable values in Table 3.9.3-11.

Snubbers are used at locations where restraints are necessary based on piping stress analysis, but thermal movement of the pipe must not be constrained. Performance selection is based on manufacturer's load capacity data and the requirement that the allowable travel of the snubber exceed the calculated pipe thermal travel. The midpoint of pipe thermal travel is set at the midpoint of the snubber travel range with hot and cold settings established accordingly. If snubbers are used to mitigate effects of operational vibration, the analytical and design methodology utilized as well as design specification requirements to assure that structural and mechanical performance characteristics and product quality are achieved will be developed and available for review.

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Each snubber assembly is accessible after installation and all adjustment features are unobstructed and visable where possible. The manufacturer's figure number, size, stroke, and load rating is mounted on each snubber.

The loading combinations for Westinghouse items are given on Table 3.9.1-2.

3.9.3.2 Pump and Valve Operability Program

3.9.3.2.1 Westinghouse Pump and Valve Operability Program

Mechanical equipment classified as safety-related must be capable of performing its function under postulated plant conditions. Equipment with faulted condition

Application: SIMPWIP

A. Author: EDS Nuclear, Inc.
220 Montgomery Street
San Francisco, California 94104

Source: EDS Nuclear, Inc.

Version: August 15, 1979

Facility: EDS Nuclear, Inc. and UCC Dallas

B. Description: SIMPWIP is a computer program for the simplified time-history analysis of pipe whip problems. The program provides a conservative estimate of the restraint forces and deformations, during the initial impact phase up to the time the piping system initially stops after impact with the restraints. SIMPWIP can handle both circumferential and longitudinal pipe rupture events. The program assumes the motions of the system to be two-dimensional and in the small-deflection range.

Extent and Limitations of its application: All routines of the SIMPWIP program are used as detailed in the description.

C. Verification: The program has been verified by comparison with the results of the hand calculated problem.