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CORE SPRAY CRACK ANALYSIS
FOR
MONTICELLO NUCLEAR GENERATING PLANT

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1.0 INTRODUCTION AND SUMMARY

During the current refueling and maintenance outage, the vessel in-service inspection identified a crack indication (Figure 1-1) on the core spray line at Monticello Nuclear Generating Plant. The indication was identified using an under water camera during the inspection in response to IE Bulletin 80-13 (Reference 1). The crack indication is located outside the shroud where the piping and junction box meet in the heat affected zone (HAZ) of the weld. The following additional information was provided by Northern States Power (NSP):

a) The crack was verified by UT inspection to be through-wall.
b) The crack is approximately 3.5 inches in length along the outside diameter of the pipe based on visual measurements.

GE Nuclear Energy has performed an evaluation to address the safety significance of the through-wall crack. The technical basis to support the continued structural integrity of the core spray line for all normal and injection conditions is provided. A discussion of the possible consequences of potential loose pieces from a cracked pipe is also presented. Finally, the consequences of a postulated Loss-of-Coolant Accident (LOCA) with a crack in the core spray piping are discussed.

1.1 CRACK LEAKAGE ESTIMATE

A bounding calculation to estimate the leakage through the crack, presented in Section 2, demonstrated that the total leakage is well within the margin inherent in the core spray system design and performance evaluations. The results indicate that for this crack configuration including the postulated crack growth, the total flow leakage is conservatively estimated to be 24 gpm.

1.2 STRUCTURAL ANALYSIS

The structural analysis, presented in Section 3, concludes that the integrity of the core spray piping will be maintained for all conditions of operation over the next operating cycle. In addition, potential causes of cracking are discussed, and based on the information available, it is expected that the most likely cause is Intergranular Stress Corrosion Cracking (IGSCC).

1.3 LOST PART ANALYSIS

Because continued sparger structural integrity was demonstrated, lost parts (loose pieces) are not expected. Nevertheless, a lost parts analysis has been performed and is presented in Section 4. It is concluded that the probability of unacceptable flow blockage of a fuel assembly or unacceptable control rod interference due to lost parts is negligible. The potential for corrosion or other chemical reactions with reactor materials does not exist because the piping material is designed for in-vessel use. It is also shown that loose pieces are not expected to cause damage to the other reactor pressure vessel internals.

1.4 EFFECT ON LOCA ANALYSIS

Section 5 presents the results of the LOCA analysis. The results show that the inherent conservatisms present in current LOCA analyses more than offset the small amount of leakage estimated through the crack. It is concluded that no change to the present Maximum Average Plant Linear Heat Generation Rate (MAPLHGR) for Monticello is required.

1.5 CONCLUSIONS

A detailed evaluation of the Monticello core spray crack has been performed. This evaluation included structural, lost parts and LOCA analyses to determine the impact on plant operation with the crack in the core spray piping. Based on the analysis, it is concluded that Monticello can safely operate in this condition during the next fuel cycle, and that no operational changes or restrictions are required during that period.

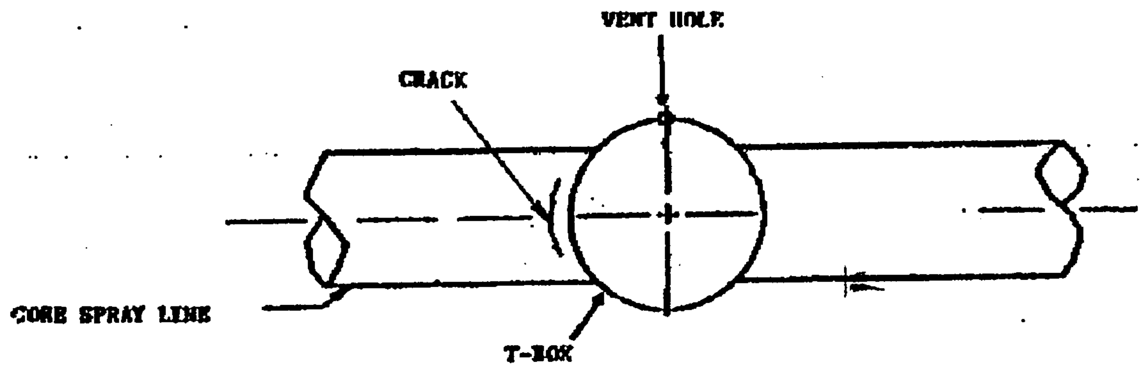

Figure 1-1 T-Box with Crack & Vent Hole

2.0 CRACK LEAKAGE ESTIMATE

There are no direct measurements of leakage from the crack during the operation of the core spray system. However, from previous analyses and tests performed for the cracks observed in other BWRs, it is possible to establish an upper bound leakage for the crack identified at the Monticello Plant.

The significance of previous crack occurrences at other BWRs has been assessed by both visual inspections and air-bubble tests. Based upon these inspections and tests, the upper bound leakage was estimated to be less than half the leakage through the 1/4 inch vent hole present in the T-box. (The vent hole is part of the original piping design and is included to allow the release of any non-condensable which could collect in the core spray piping). The video from the Monticello inspection indicated that the crack in the Monticello core spray line is conservatively estimated to be 3.5 inches in length. Consequently, it is conservative to assume that the maximum leakage from the Core Spray Line crack is approximately 24 gpm, assuming a 180° (≈8.5") through-wall crack.

2.1 CURRENT LEAKAGE RATE

The vent hole is a 1/4 inch hole present in the T-box. The leakage rate through the vent hole is estimated assuming incompressible Bernoulli flow through the hole:

$$Q = CA\sqrt{2g_c \Delta P / \rho}$$

where, C = flow coefficient (assumed to be 0.6 for an abrupt contraction)
A = area
ρ = mass density of fluid
ΔP = pressure difference across the pipe/vent

The flow rate through the vent hole was determined utilizing a bounding pressure of 125 psig across the core spray line (actual pressure = 111 psig). This corresponds to the differential pressure expected during the rated core spray flow conditions. Utilizing the equation above, the estimated leakage rate through the vent hole during a LOCA was determined to be less than 13 gpm. Therefore, during the core spray injection phase of a LOCA, the total leakage through the crack is expected to be less than 5 gpm (less than one-half of the vent hole leakage).

2.2 MAXIMUM ESTIMATED CRACK LEAKAGE

In order to estimate the maximum leakage expected through the crack, the configuration for a 180° through-wall crack was used. This configuration was considered to be the upper bound based on the crack arrest results of Section 3.0. A crack width of 0.01 inch was conservatively assumed based on the results of Linear Elastic Fracture Mechanics (LEFM) methods which showed the crack opening to be < 0.01 inch under the applied loads described in Section 3.0. Using the methods of Section 3.2 for these loads and the 180° through-wall crack configuration, the leakage was determined to be 24 gpm.

It was also estimated that the current crack size is expected to grow less than 1.2 inches during the next 18 month cycle (4.7 inches in length). This result is based on the consideration of both IGSCC and fatigue crack growth. For IGSCC, conservative crack growth rates at moderate conductivity for 304 stainless steel were assumed (4×10^{-5} in/hour), and crack growth from both ends of the crack was considered. The assumed IGSCC crack growth rate is considered conservative for two reasons:

1) The assumed value of crack growth is based on normal water chemistry conditions. The Monticello plant is expected to operate with hydrogen water chemistry. Although the electro chemical potential (ECP) in the area of the core spray line is not expected to meet the necessary level for full IGSCC protection, the crack growth rate is likely to be substantially lower than the assumed value of 4×10^{-5} in/hour.

2) Using the NRC curve, the assumed crack growth rate of 4×10^{-5} in/hour is predicted at a stress intensity factor, K, of 25 $ksi(in)^{1/2}$. Since the subject crack is through wall (thus, the weld residual stress induced K is expected to be very low) and the applied stresses are low, the K values are expected to be less than 25 $ksi(in)^{1/2}$ for realistic crack geometries.

Thus, even after 18 months of additional operation, the crack length is expected to be less than 100° of the pipe circumference. Therefore, the leakage estimate of 24 gpm for a 180° crack length is conservative for the next cycle.

3.0 CORE SPRAY PIPE STRUCTURAL INTEGRITY

The structural integrity aspects of the core spray piping have been reviewed to assess: a) the potential cracking mechanism, and b) the impact the crack could have on the structural integrity of the piping. Structural analyses were performed to determine the potential sources of stress in the piping, the potential causes of cracking, and the likelihood of crack propagation.

Although there is currently not enough information to definitively determine the mode of cracking, it is expected that the crack is due to an Intergranular Stress Corrosion Cracking (IGSCC) mechanism. The results of these assessments are discussed below:

3.1 POTENTIAL CAUSE OF CRACKING AND LIKELIHOOD OF CRACK ARREST

3.1.1 Cracking Mechanism

Cracks in the core spray line could be due to either thermal fatigue or intergranular stress corrosion cracking (IGSCC). At this point, there is not enough information to determine the mode of cracking, definitively. Consequently, the following discussion addresses the implications of the observed cracking assuming each mechanism separately.

3.1.2 Thermal Fatigue

The feedwater (FW) sparger is located above the core spray piping in the annulus. Some high frequency thermal cycling could occur near the core spray piping because of the turbulent mixing of the cooler feedwater from the FW sparger and the hotter downcomer flow. The magnitude of temperature cycling is dependent on the feedwater temperature and flow rate. Fatigue initiation due to thermal cycling is not only a function of the temperature difference between the feedwater and downcomer flow, but also depends on the time duration over which the cycling occurs. With this cyclic mechanism, crack initiation is most likely to occur near a weld because of the high residual stresses present. Even if fatigue initiation does occur because of rapid thermal cycling, the cracking is likely to be confined to the outside surface of the pipe since the thermal stresses attenuate rapidly through the thickness of the pipe. Thus, if thermal fatigue is the initiation mechanism, extensive fatigue crack growth is unlikely (further growth can occur by IGSCC since the fatigue crack acts as a crevice). Therefore, the IGSCC induced growth analysis described below is a bounding crack growth assessment as shown later in this section.

3.1.3 Intergranular Stress Corrosion Cracking

The core spray line in the Monticello plant where the crack is located is made of type 304 stainless steel. Type 304 stainless steel can sensitize, leading to IGSCC in the weld HAZ. Local cold work which could have occurred during fabrication can also contribute to IGSCC initiation. The Monticello plant has been operating for some time with hydrogen water chemistry (HWC). However, the HWC is not very effective in the area in which the core spray line is located. The electro chemical potential (ECP) at the core spray line is expected to be above the threshold (-230 mV), below which full IGSCC protection is assured. Thus, if the observed cracking is of IGSCC origin, some crack growth during future operation can not be ruled out. A conservative estimate of this potential crack growth and its effect on the structural integrity of the core spray line is discussed in Section 3.2.

3.2 STRUCTURAL INTEGRITY

3.2.1 Summary

All identified stresses expected during normal reactor operation were found to be small. Based upon a review of these stresses, it is concluded that the structural integrity of the piping with the crack will be maintained during core spray injection. The stresses considered include those due to downcomer flow impingement loads, seismic loading, pressure, weight and thermally induced loads.

Although the normal operating loads by themselves do not result in stresses which are sufficient to cause IGSCC initiation, the addition of the weld residual stresses coupled with local cold work could result in exceeding the initiation threshold. Once initiated, the normal operating load stresses and the residual stresses could cause subsequent growth of the induced cracks.

In order to determine the integrity of the core spray line with the crack, a crack arrest evaluation was performed. The stresses due to pipe restraint were also included in this evaluation. Because the applied normal loading is predominantly displacement controlled, the stresses relax as the crack grows and the compliance (or flexibility) of the pipe increases. The results of the analysis showed that when the crack reaches 180° of the circumference, the compliance is reduced sufficiently to relieve almost all of the displacement controlled stresses. Consequently, the crack growth is expected to be negligible or at virtual arrest prior to reaching 180°. (The current through-wall affected area is less than 90° of the piping circumference.)

3.2.2 Crack Arrest Evaluation

Stresses in the core spray piping due to bracket restraint are governed by the applied displacement and the compliance of the pipe. Since the displacement is fixed, the compliance change with crack growth could lead to crack arrest. This is comparable to crack arrest in a bolt loaded wedge-opening-loading (WOL) specimen in stress corrosion tests.

Figure 3-1 shows the variation of compliance with crack length for a pipe subjected to bending. The compliance was determined using the relationship between strain energy release rate, G; and the compliance change per unit area of crack extension dc/dA (Reference 4). For the cracks in the core spray line, L/D is expected to be in the range $0 < L/D < 40$. Figure 3-1 shows that the compliance of the pipe increases by a factor of ten when more than 30% of the pipe is cracked. Therefore, for the given initial displacement, the stress in the core spray line and the applied stress intensity factor would decrease by a factor of ten when more than 30% of the pipe circumference is cracked. Clearly, when the crack length exceeds this value, the restraint stresses become negligible and crack arrest is expected. Therefore, crack arrest is expected before the crack grows to 180°.

3.2.3 Allowable Flaw Size Determination

Even though the cracks are expected to self arrest at 180° under the sustained displacement controlled loading as discussed in Section 3.2.2, an evaluation was performed to determine the maximum allowable circumferential through-wall flaw size in the core spray pipe. This analysis will therefore provide an assessment of the safety margin in the pipe due to primary loads such as deadweight, pressure, flow impingement and seismic.

The acceptable through-wall flaw size of the core spray line is determined utilizing the net section collapse formulation of Reference 5. To apply this methodology, primary membrane stresses in the longitudinal direction and primary bending stresses were determined for the T-box region of the pipe. A finite element model of the core spray pipe was developed to obtain the stresses due to deadweight, seismic and reactor vessel downcomer flow impingement on the pipe at the location of interest. The resulting stresses were then combined with the stresses due to pressure and core spray flow loads in order to get the total stresses acting on the pipe. Stresses due to water hammer loads were considered insignificant and neglected in this analysis. This is based on the fact that the core spray inlet valve ramps open over a period of twenty seconds upon system actuation. Additionally, the piping is full of water during actuation because of the presence of the vent hole on the top of the T-box. Previous analyses have shown that the water hammer loads in the core spray line were calculated to be less than 20 pounds of axial load on the pipe. Stresses due to thermal mismatch were evaluated and found to be insignificant. By applying these resulting primary stresses, it was shown that the core spray pipe can tolerate a crack up to 240° through-wall at the T-box location without incipient failure.

3.2.3.1 Analysis and Results

A finite element model of the core spray line configuration was constructed using the ANSYS computer code (Reference 6). A sketch of the finite element model is shown in Figure 3-2. The following boundary conditions were applied to the model:

Nodes 1, 49, 54: completely fixed
Nodes 13, 37 : fixed in vessel radial direction to account for bolted vessel clamps.

Loads due to the weight of the pipe (including captured water in the pipe) were applied to the model along with vertical and horizontal seismic loads and reactor vessel downcomer flow impingement loads. Calculations of these loads are given in Appendix A. The largest resulting stresses in the region of the T-box (nodes 24-26) were used from the finite element model results. These stresses were then combined with the stresses due to pressure and core spray flow loads. The resulting total stresses are shown in Table 3.1. Note that loads due to thermal mismatch of the core spray line and reactor vessel need not be included as they are secondary in nature.

TABLE 3.1

RESULTING PRIMARY STRESSES AT TEE BOX REGION

Membrane Stress, Pm	1306 psi
Bending Stress, Pb	1606 psi

The stresses of Table 3.1 are utilized to determine the acceptable through-wall flaw size based on the methods of Reference 5. The acceptable flaw size is determined by requiring a suitable design margin on the critical flaw conditions. The critical flaw size is determined by using limit load concepts. It is assumed that the pipe with a circumferential crack is at the point of incipient failure when the net section at the crack develops a plastic hinge. Plastic flow is assumed to occur at a critical stress level, σ_f, called the flow stress of the material. For ASME Code analysis, σ_f may be taken as equivalent to $3S_m$. This results in considerable simplification of the analysis.

Consider a circumferential crack of length, $l = 2R\alpha$, and constraint depth, d, located as shown in Figure 3-3. In order to determine the point at which collapse occurs, it is necessary to apply the equations of equilibrium assuming that the cracked section behaves like a hinge. For this condition, the assumed stress state at the cracked section is as shown in Figure 3-3 where the maximum stress is the flow stress of the material, σ_f. Equilibrium of longitudinal forces and moments about the axis gives the following equations:

(For neutral axis located such that $\alpha + \beta < \pi$)

$$\beta = [(\pi - \alpha d/t) - (Pm/\sigma_f)\pi]/2$$

$$P_b = (2\sigma_f/\pi)\ (2 \sin\beta - d/t \sin\alpha)$$

where, t = pipe thickness, inches.

α = crack half-angle as shown in Figure 3-3.

β = angle that defines the location of the neutral axis.

Using the stresses of Table 3.1 and a d/t ratio of 1.0 (through-wall flaw), the allowable through-wall crack for which failure by collapse might occur is 240°.

3.2.3.2 Temperature Gradient Evauation

A fatigue crack growth analysis due to thermal gradients across the core spray piping was conducted using conservative values for the temperature differences expected between the inside and outside surfaces of the core spray pipe. Two events were conservatively considered for this analysis. The first was a HPCI injection in which cold water (100°F) from the feedwater spargers impinges on the hot core spray line (550°F). Conservatively assuming the temperature at the bottom of the core spray piping remains at 550°F, a thermal bending stress results across the pipe cross section. The second event considered was the actuation of the core spray system. In this event, cold water (50°F) is injected through the hot core spray line (550°F) which induces a uniform thermal membrane stress throughout the pipe cross section. This analysis showed that a conservative estimate of the fatigue crack growth due to HPCI and core spray injections is about 0.14 inches. Details of this analysis are provided in Appendix A. This growth is minimal when compared to the crack growth predicted due to the IGSCC mechanism (predicted to be 1.05 inches). Based on this conservative evaluation, fatigue crack propagation as a result of severe thermal transients is negligible when compared to that from IGSCC.

3.2.3.3 Flow Induced Vibration Evaluation

A flow induced vibration (FIV) evaluation was conducted considering field measured data from a similarly designed core spray system. In order to eliminate FIV concerns, it is required that the natural fraquency of the system be greater than three times the vortex shedding frequency. The vortex shedding frequency for this system due to downcomer flow was calculated to be 5.1 Hz. A natural frequency of 27.5 Hz was obtained for the core spray line from the limited field data. To asses the potential change in these values as a result of a cracked core spray line, additional analysis was conducted assuming a 180° through-wall crack. The ratio of the compliance of the the uncracked line to the cracked line was calculated to be 0.649. Given that the natural frequency is proportional to the square root of the stiffness (stiffness = inverse of the compliance), this leads to a predicted 20% decrease in the natural fraquency (22 Hz). Since this adjusted value of the natural frequency still remains greater than three times the vortex shedding frquency (15.2 Hz), the results of this evaluation show that no degradation as a result of FIV is expected.

3.3 SUMMARY AND CONCLUSIONS

The potential sources of stress in the piping resulting from normal operation and operation during postulated Loss of Coolant Accidents were reviewed. Potential causes of cracking, thermal fatigue and IGSCC, and the likelihood of crack propagation were also evaluated. It is expected that the crack was caused by IGSCC.

Because of the predominant secondary stresses, the crack can be expected to arrest prior to reaching 180°. An assessment was made to determine the critical flaw size of the core spray pipe by treating stresses associated with the design loadings as primary stresses and performing a net section collapse evaluation. The results of this evaluation confirm that a through-wall crack of up to 240° around the circumference would not cause pipe failure. This length is much greater than the maximum estimated crack length at the end of the next fuel cycle (predicted to be 4.7 inches, 100°). Therefore, it is concluded that the structural integrity of the piping with a crack will be maintained for all conditions of normal operation for the next operating cycle.

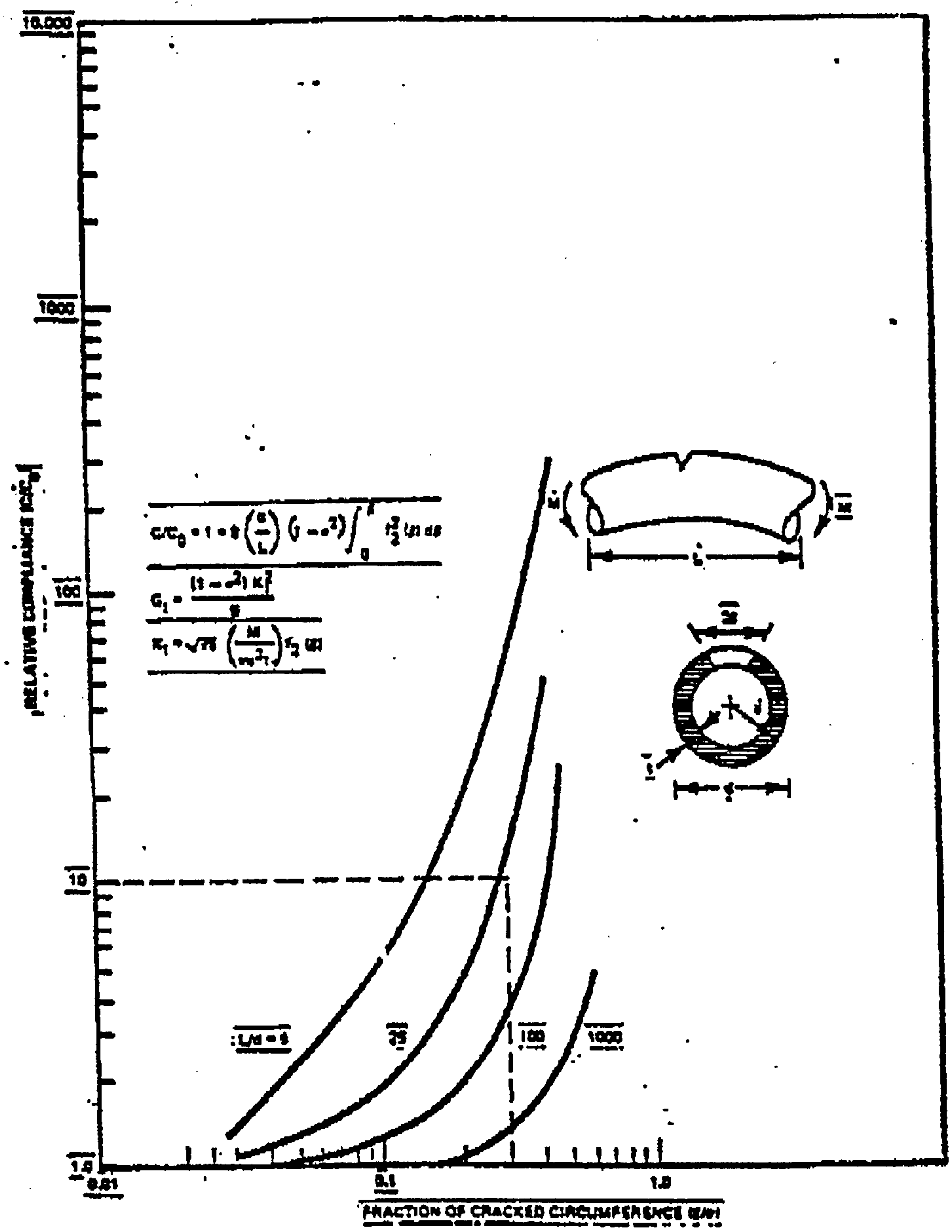

FIGURE 3-1: Compliance Change, Cracked Pipe

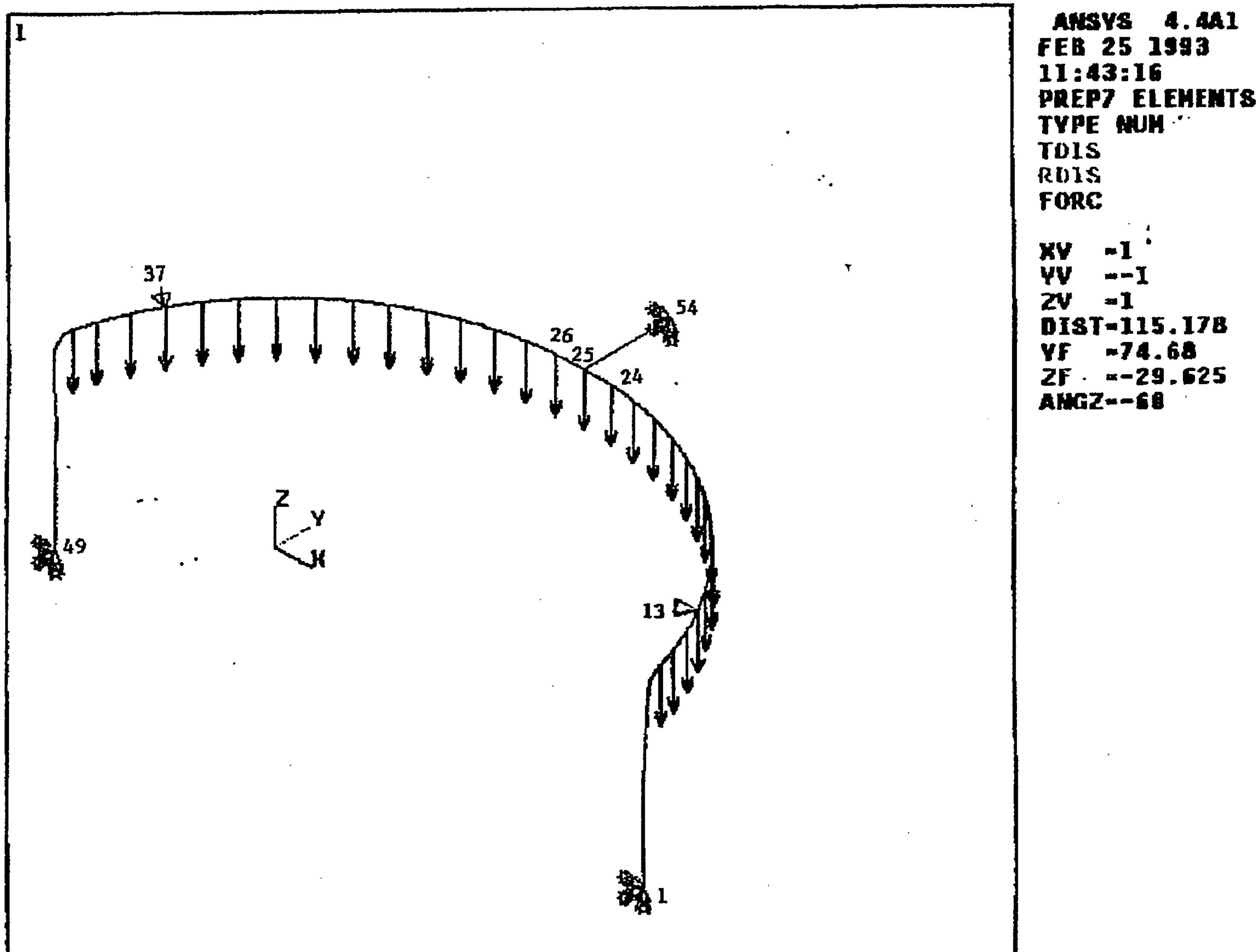

Figure 3-2:
MONTICELLO CORE SPRAY LINE ANALYSIS

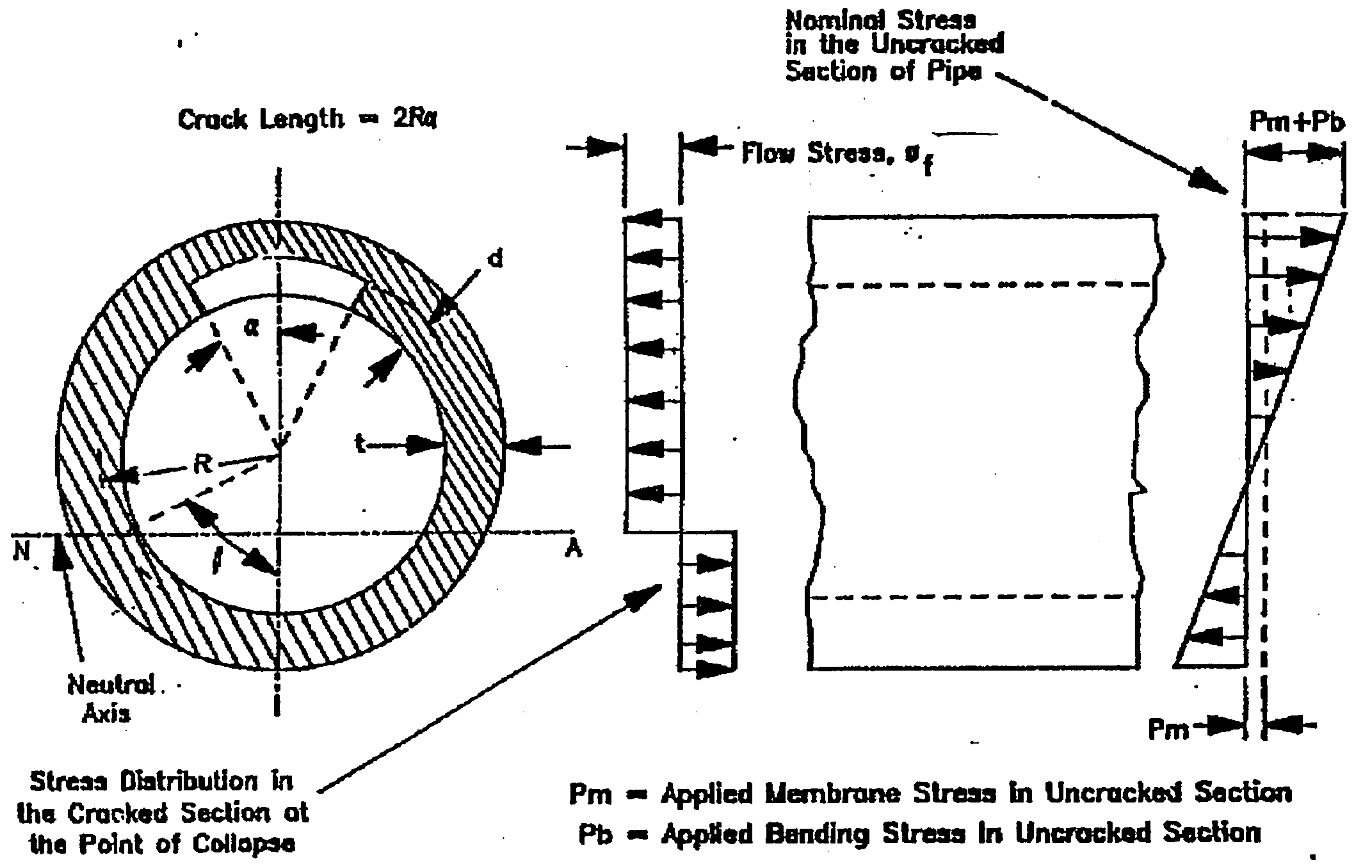

FIGURE 3-3

STRESS DISTRIBUTION IN A CRACKED PIPE AT THE POINT OF COLLAPSE

4.0 LOST PARTS ANALYSIS

4.1 INTRODUCTION

Based on the structural analysis given in Section 3, it is expected that the Monticello Nuclear Generating Plant core spray pipe will not break and consequently, will not result in loose pieces in the reactor. However, an evaluation of the possible consequences of a potential loose piece is presented in this section.

4.2 LOOSE PIECE DESCRIPTION

Since a piece has not been lost, it cannot be uniquely described. Two different types of loose pieces are postulated:

1) a section of core spray pipe, and;
2) a small piece of the core spray pipe.

4.3 SAFETY CONCERNS

The following safety concerns are addressed in this analysis:

1) Potential for corrosion or other chemical reaction with reactor materials.
2) Potential for fuel bundle flow blockage and subsequent fuel damage.
3) Potential for interference with control rod operation.
4) Potential for damage to the reactor internals.

4.4 EVALUATION

The above safety concerns for the postulated loose pieces are addressed in this section. The effect of these concerns on safe reactor operation is also addressed.

4.4.1 General Description

Since the core spray pipe with the crack is in the annular region of the reactor pressure vessel, this evaluation assumes that any potential loose piece generated from the core spray pipe will most likely sink into the downcomer region.

For a loose part to reach, and potentially block the inlet of a fuel assembly (Figure 4-1), it would have to be carried into the lower plenum. To accomplish this, it would have to be carried by the recirculation flow through the jet pump nozzle into the lower plenum, then make a 180° turn and be carried upward to the fuel assembly inlet orifices.

For a piece of the core spray pipe to reach a control rod it must first migrate to the lower plenum, pass through the fuel inlet orifice, and traverse the fuel bundle. Then, it must either fall through the restrictive passage between two fuel channels, or fall through an opening between the peripheral bundles and the core shroud. Both of these potential paths are unlikely.

The core spray pipe is fabricated from Type-304 grade stainless steel and all parts of the core spray pipe are designed for in-reactor service. Consequently, there is no postulated loose part that will cause any corrosion or other chemical reaction with any reactor material.

4.4.2 Postulated Loose Pieces

4.4.2.1 Core Spray Pipe

The core spray pipe is 5 inch Schedule 40S pipe. In order to generate a loose piece of pipe, a minimum of two through-wall cracks would have to propagate 360° around the pipe.

If a pipe segment were postulated to break off, it would sink into the downcomer region. Since it cannot fit through the jet pump, it cannot enter the lower plenum, and therefore will not cause any flow blockage at the fuel inlet orifice. Since it is too large to fit between fuel channels, it cannot cause any interference with control rod operations. Nevertheless, due to the slow propagation rate of potential cracks, and based on previous experience with cracks in core spray spargers, it is judged that a piece of the piping will not break off and become loose.

4.4.2.2 Small Pieces

In order to generate small pieces of the core spray pipe, both longitudinal and circumferential through-wall cracking must occur. A small piece could then sink, be carried into the down comer annulus, pass through the jet pump and enter the lower plenum. A piece that entered the lower plenum would probably be driven by the jet pump flow to the bottom of the reactor pressure vessel where it would be expected to remain. However, a small piece < 0.4 inches could be carried by the flow up to the fuel inlet orifices. The orifice sizes in the Monticello Plant vary from approximately 1.4 to 2.1 inches in diameter (Reference Figure 4-2).

Given the dimensions, the piece would pass through the inlet orifices and be trapped at the lower tie plate grid and cause some bundle flow blockage. However, the flow blockage is much less than that required to initiate critical boiling transition in the bundle. Multiple pieces migrating to the same bundle may result in critical flow blockage, but the probability for such an occurrence is extremely low.

It is also very unlikely that a small piece could lift and migrate from the lower plenum through the fuel bundle and fall into the control rod guide tube. In order to do this, the piece would have to be so small that it could pass through all the bundle spacers and out through the top of the bundle. Such a small piece would not present any potential for control rod interference.

Figure 4-3 shows a typical unit cell of four fuel assemblies and one control rod. The control rod moves in the gap between the fuel channels. There is a small possibility that a piece small enough to fit in the gap between the channel wall and control blade could sink and pass through the cavity between the control blade and the fuel support casting and migrate into the control rod guide tube. Should this happen the piece will most likely come to rest on the top of the velocity limiter where it is expected to remain and move only with the movement of the velocity limiter as the control rod is inserted or withdrawn. If the piece is small enough to pass between the velocity limiter and the guide tube wall it will most likely sink and come to rest at the bottom of the guide tube. Due to the hardware geometry of the control blade drive mechanism it is highly unlikely that any piece would be small enough to migrate into the control blade drive system. Thus, any potential small piece which migrates to the control rod guide tube is not expected to pose any concern for potential interference with control rod operation.

One of the licensing bases of the reactor is that with the highest worth control rod fully withdrawn the reactor can be brought to cold shutdown. Thus, unacceptable control rod interference would require multiple precisely sized pieces interfering simultaneously with control rods that are in close proximity to each other. The probability of this is judged to be insignificant.

4.5 CONCLUSIONS

The core spray pipe at Monticello Plant is expected to remain intact; therefore, it is highly unlikely that pieces of the core spray pipe will break off. From the above evaluation it is concluded that the probability for unacceptable corrosion or other chemical reaction due to loose pieces is zero. The potential for unacceptable flow blockage or other damage to the fuel assemblies is negligible. The potential for unacceptable control rod interference is negligibly small. Therefore, it is concluded that there is no safety concern posed by any postulated loose parts.

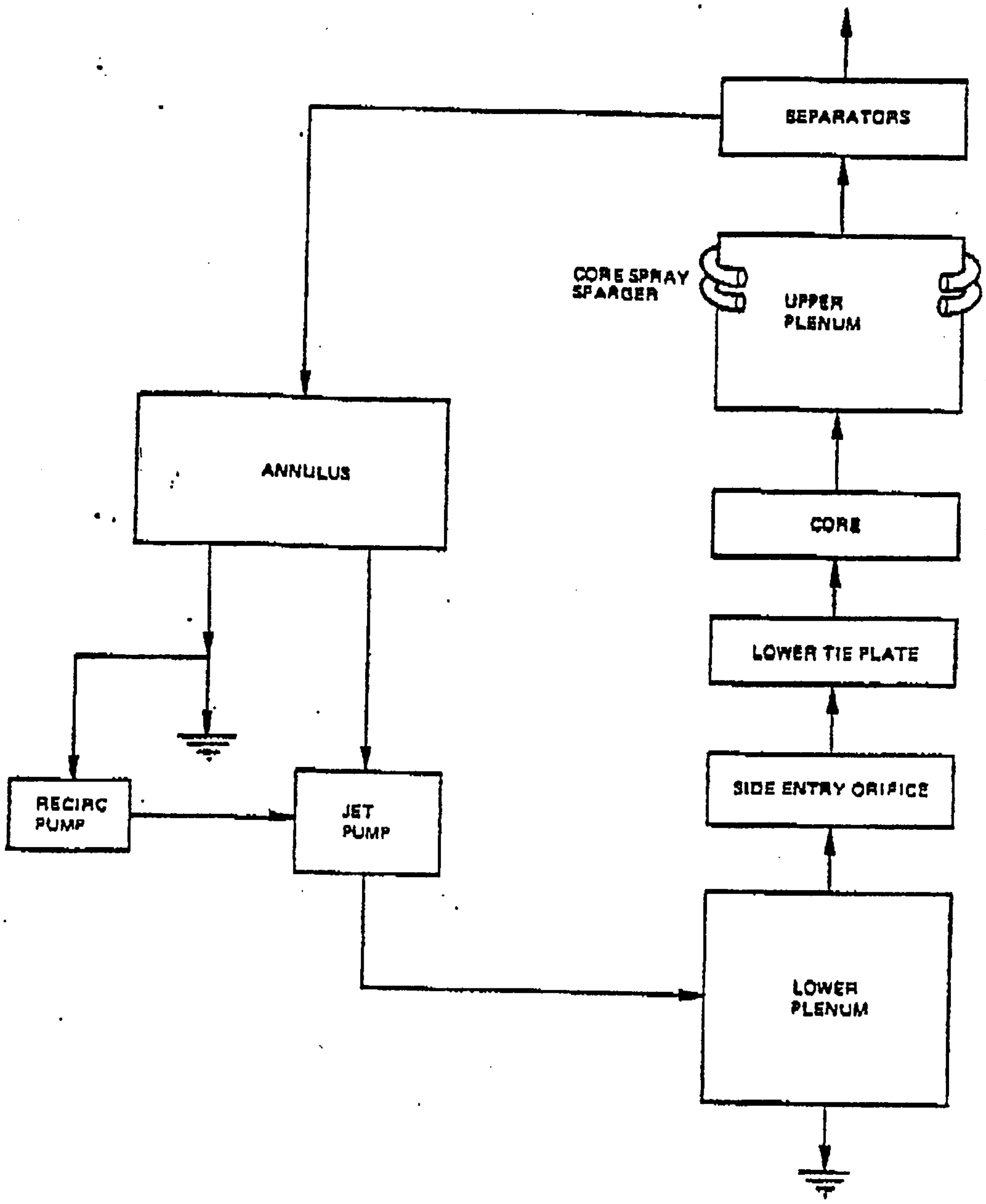

FIGURE 4-1 LOOSE PIECE POTENTIAL UPWARD FLOW PATH

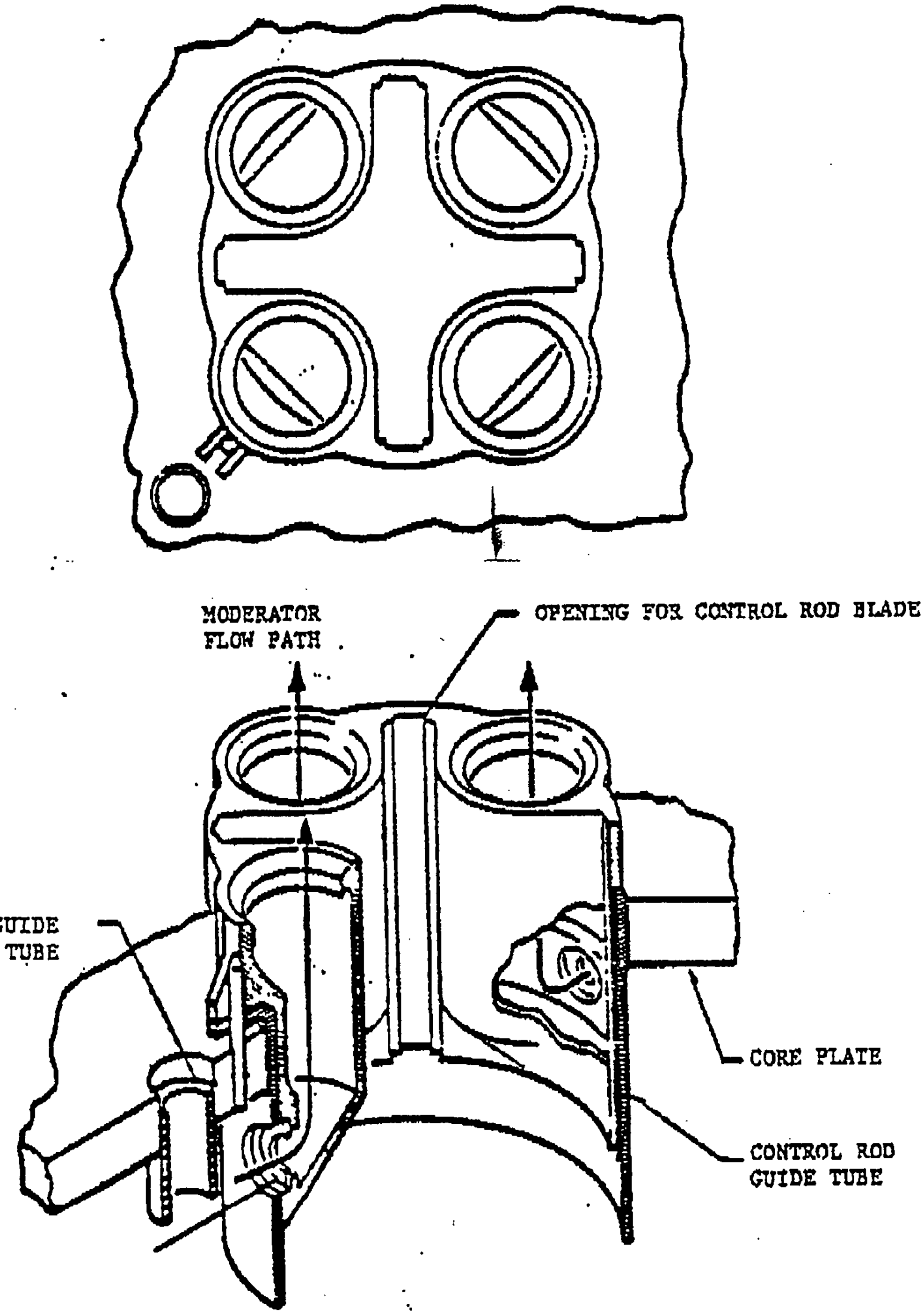

FIGURE 4-2 ORIFICED FUEL SUPPORT

FIGURE 4-3: FUEL ASSEMBLIES AND CONTROL ROD MODULE

5. IMPACT ON ECCS ANALYSIS

5.1 IMPACT OF CORE SPRAY LINE LEAK ON SAFER/GESTR-LOCA ANALYSIS

For Monticello Nuclear Generating Station only two single failure candidates are potentially limiting for ECCS system performance following a LOCA. These are associated with the limiting break which is a recirculation pipe break (Reference 2). These limiting cases are:

A. Battery Failure. This postulated failure leaves 1 Core Spray + 2 Low Pressure Coolant Injection (LPCI) + the Automatic Depressurization System (ADS) operable;

B. LPCI Injection Valve Failure (LPCI IV). This postulated failure leaves 2 Core Spray + High Pressure Coolant Injection (HPCI) + the ADS operable.

The CS flow rate assumed for each loop in the above referenced Monticello SAFER evaluations was 2700 gpm (Reference 3, p. 5-2). This value reflects the CS flow rate which is assumed to actually inject inside the core shroud, and this is 320 gpm less than the expected system performance of 3020 gpm per core spray system (Reference 3, p. 5-4). This margin of 320 gpm in the assumed CS flow in the SAFER evaluation is much greater than the total estimated leak flow of 24 gpm from a 180° crack in the CS line plus the 13 gpm leak through the vent hole located in the top of the T-box. Thus, the SAFER/GESTR-LOCA analysis for both the nominal and Appendix K assumptions as documented in References 2 and 3 covers, with significant margin, the estimated leak through the CS line crack.

5.2 IMPACT OF POSTULATED FAILURE OF CORE SPRAY LINE AT CRACK LOCATION

Additional analyses for the above limiting single failure cases were performed considering the unlikely failure of the CS line at the crack location. With this postulated failure of the core spray line all core spray flow for this loop was assumed to drain into the downcomer region. These analyses were performed for the recirculation suction line break with nominal assumptions (Reference 3, Table 3-1). The Peak Cladding Temperature (PCT) for these cases were calculated to be less than 1330°F in the case of battery failure and 1650°F in the case of LPCI injection valve failure. The LPCI injection valve failure is the more limiting case because it has a much lower ECCS makeup flow rate than the battery failure. Other postulated failures are not specifically considered because they all result in more ECCS capacity than the above assumed failures (Reference 2, Table 4-2). For smaller breaks with the additional CS line failure, the PCT is expected to be lower than the above calculated values since the core inventory loss will be less and hence the time to reflood will be less compared to the large break scenarios. In addition, at some break sizes the CS makeup flow into the annulus with this assumed failure will exceed the break flow rate, allowing the downcomer to refill and the CS water injected into the downcomer region to reach the core via the jet pumps/lower plenum.

6.0 REFERENCES

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APPENDIX A:

STRUCTURAL ANALYSIS OF THE MONTICELLO PLANT CORE SPRAY PIPE

A.1: CRACK GROWTH DUE TO IGSCC

The stress results given in Table 3-1 of Section 3.2.3.1 of this report are developed in this Section of the Appendix. The stresses were determined by applying dead weight, seismic and flow impingement loads to the finite element model developed for the core spray line (see Figure 3-2). The calculation of these loads is given here. Also included are the calculations of the stresses due to pressure and flow loads as well as the total combined primary stresses given in Table 3.1.

Weight of Lines

An equivalent density was input to the ANSYS finite element model to include both the weights of the pipe and captured water. This equivalent density is calculated below:

Metal density = 0.2879 lb/in^3
Water density = 62.4 lb/ft^3 = 0.0361 lb/in^3

Pipe size = 5 inch schedule 40S Stainless Steel
OD = 5.563", t = 0.258", ID = 5.047"

Metal area = $(\pi/4)(5.563^2 - 5.047^2) = 4.3$ in^2
Water area = $(\pi/4)(5.047^2) = 20.0$ in^2

Metal weight = (0.2879 lb/in^3) (4.3 in^2) = 1.238 lb/in
Water weight = (0.0361 lb/in^3) (20.0 in^2) = 0.722 lb/in

Adjusted density = (total weight)/(metal area)
= (1.238 + 0.722)/4.3
= 0.456 lb/in^3 = 0.0012 slugs/in^3

Impingement Loads (90° Deflection of Flow):

$F = PA = \rho V^2 DL/g$ $D = 5.563''/12 = 0.464$ ft

Assume downcomer flow, V = 5 ft/sec (conservative)

For water, $\rho = 62.4$ lb/ft^3

$F/L = \rho V^2 D/g = (62.4)(5^2)(0.464)/32.2 = 22.5$ lb/ft $= 1.87$ lb/in

The nodes of the finite element model are spaced 5° apart. Thus, the following load will be applied to all nodes comprising the horizontal arms of the core spray line (nodes 10 - 40):

Node spacing $= R\theta = (99.25'')\,(5°)\,(\pi/180°) = 8.66''$
Load per node = (1.87 lb/in) (8.66") = 16.2 lb

Seismic Loads:

From the Monticello FSAR, the vertical acceleration for an Operating Basis Earthquake (OBE) is 0.04 g and the horizontal acceleration is 0.13 g. In order to provide conservative and bounding results, the seismic coefficients were doubled to obtain results for a Safe Shutdown Earthquake (SSE) and a safety factor of 1.5 was added. Based on a review of those coefficients, the following were selected for use in this analysis:

Vertical = 0.12 g
Horizontal = 0.39 g

The following accelerations were therefore applied to the finite element model:

Total vertical acceleration = Weight + Seismic
= 1.0 g + 0.12 g
= 1.12 g = 432.3 in/sec^2

Total horizontal acceleration = 0.39 g = 150.5 in/sec^2

The horizontal acceleration was applied in both the X and Y directions of the model such that the resultant was 150.5 in/sec². Thus, the horizontal acceleration applied to both directions of the model was:

$$150.5/\sqrt{2} = 106.4 \text{ in}/\text{sec}^2$$

<u>Pressure/Flow Loads:</u>

Assumed flow = (Rated flow)*2 = 3020 gpm *2 = 6040 gpm through the core spray line

$$Q = (6040 \text{ gpm})\ (1 \text{ min}/60 \text{ sec})\ (1 \text{ ft}^3/7.48 \text{ gal}) = 13.49 \text{ ft}^3/\text{sec}$$

$$F = \rho Q/2(V) = \rho Q/2(Q/A) = (62.4)\ (13.49^2)/[(32.2)(\pi/4)(5.047/12)^2] = 2{,}538 \text{ lb}$$
$$\Delta P = 150 \text{ psi @ } 6040 \text{ gpm}$$

<u>Stresses Due to Loads:</u>

Pressure:

$$\sigma_p = (150)\ (\pi/4)\ (5.047^2) / [(\pi/4)\ (5.563^2 - 5.047^2)]$$
$$= 698 \text{ psi}$$

Flow Load:

$$\sigma_F = F/A = 2{,}538/[(\pi/4)\ (5.562^2 - 5.047^2)]$$
$$= 590 \text{ psi}$$

Impingement, weight and seismic:

Stress due to the above loading was determined using the finite element model of the internal core spray piping. From the ANSYS results, the maximum of the stresses at nodes 24-26 were used since they are in the area of the cracks. The maximum stresses are given below:

σ_{DIR} = Axial Stress = 17.9 psi
σ_{BEND} = Bending Stress = 1602 psi
σ_{TOR} = Torsional Stress = 106.6 psi
σ_{TAU} = Shear Stress = 142.4 psi

Combining all of the primary stresses, the following values are obtained:

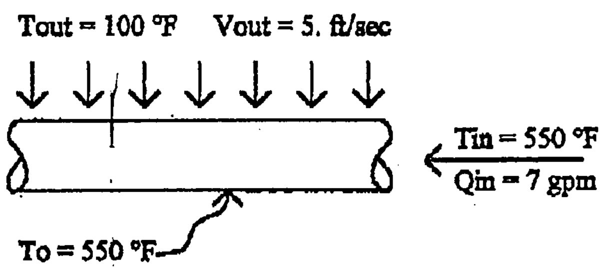
$$\text{Primary Membrane} = P_m = \sigma_P + \sigma_p + \sigma_{DIR} = 590 + 698 + 17.9 = 1306 \text{ psi}$$

$$\text{Primary Bending} = P_b = \sqrt{\sigma_{BEND}^2 + \sigma_{TOR}^2} = \sqrt{1602^2 + 106.6^2} = 1{,}606 \text{ psi}$$

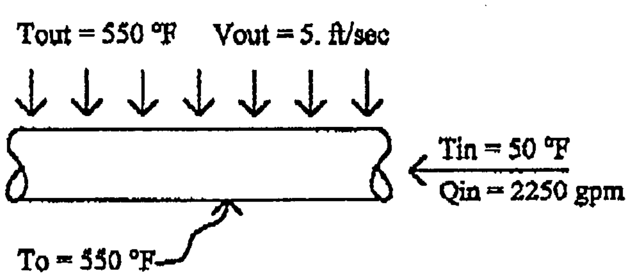
The shear stress σ_{TAU}, is small and its effect is negligible so it is not included.

A.2: FATIGUE CRACK GROWTH

The results of the fatigue crack growth analysis due to thermal gradients across the core spray piping presented in Section 3.2.3.2 are developed in this section of the Appendix. The first event considered is a HPCI injection in which cold water (100°F) from the feedwater spargers impinges on the hot core spray line (550°F). Conservatively assuming the temperature at the bottom of the core spray piping remains at 550°F, a thermal bending stress is applied across the pipe cross section. This condition is approximated by the following sketch:



The second event considered is the actuation of the core spray system. In this event, cold water (50°F) is injected through the hot core spray line (550°F) which induces a uniform thermal membrane stress throughout the the pipe cross section. This condition is approximated by the following sketch:



Heat transfer coefficients

The heat transfer coefficients for each event were computed using classical heat transfer methods. For the HPCI injection event, the outer heat transfer coefficient is calculated assuming a cylinder in turbulent cross flow. The heat transfer coefficient for a cross flow fluid temperature of 100°F is,

$$h_{out} = 992 \text{ Btu/hr ft}^2 \text{ °F}$$

The inner surface heat transfer coefficient assuming fully developed laminar flow (@ 550°F) due to leakage through the vent hole is calculated to be,

$$h_{in} = 2.83 \text{ Btu/hr ft}^2 \text{ °F}$$

For the core spray injection event, the the outer heat transfer coefficient is again calculated assuming a cylinder in turbulent cross flow. The heat transfer coefficient for a cross flow fluid temperature of 550°F is,

$$h_{out} = 1497 \text{ Btu/hr ft}^2 \text{ °F}$$

The inner surface heat transfer coefficient is calculated assuming turbulent flow through a cyclinder (@ 50°F).

$$h_{in} = 4251 \text{ Btu/hr ft}^2 \text{ °F}$$

Temperature gradients across wall of pipe

Using the heat transfer coefficients calculated, the temperature change across the pipe was calculated assuming one-dimensional, steady state conduction. For the HPCI injection event, the inner and outer temperatures of the core spray piping at the top of the pipe was calculated to be,

$$T_i \approx 100°F$$
$$T_o \approx 100°F$$

Assuming the temperature of the bottom of the pipe remains at 550°,

$$\Delta T = 550° - 100° = 450°$$

For the core spray injection event, the inner and outer temperatures of the wall were calculated to be,

$$T_i \approx 50°F$$
$$T_o \approx 550°F$$

Based on these results, the average change in temperature across the wall is,

$$\Delta T = (500° + 0°)/2 = 250°$$

<u>Thermal stresses</u>

The thermal stresses for both events were calculated using the expression,

$$\sigma = \frac{E\alpha\Delta T}{2(1-\nu)}$$

The thermal bending stress due to the HPCI injection is calculated to be,

$$\sigma = 76.9 \text{ ksi}$$

The thermal membrane stress due to the core spray injection is calculated to be,

$$\sigma = 42.7 \text{ ksi}$$

<u>Stress intensity factors</u>

Stress intensity factor were calculated using the thermal stresses calculated for each event. For the HPCI injection, the expression used for K_I is (Reference 7):

$$K_I = \frac{\sigma\sqrt{\pi a}(1-\nu)}{(3+\nu)}$$

where

σ = Bending stress (ksi) = 76.9 ksi a = Crack length (in) = 2.35"

ν = Poissons ratio = .287

$$K_I = \frac{76.9\sqrt{\pi 2.35}(1+0.287)}{(3+0.287)} = 81 \text{ ksi}\sqrt{\text{in}}$$

For the core spray injection, the expression used for K_I is (Reference 8):

$$K_I = \sigma\sqrt{\pi a}(G_m - G_b)$$

where

σ = Bending stress (ksi) = 42.7 ksi

a = Crack length (in) = 2.35"

Gm = Membrane stress contribution = 1.62

Gb = Bending stress contribution = -.14

$$K_I = 42.7\sqrt{\pi 2.35}(1.62-(-0.14)) = 202 \text{ ksi}\sqrt{\text{in}}$$

Crack growth rate

Using the values of K_I calculated above, the fatigue crack growth rate was determined using experimentally determined curves for 304 stainless steel in a simulated BWR environment (Reference 9). Conservatively assuming $K_{min} = 0$, ΔK is equal to $K_{I\,max}$. From Figure 4-1. of Reference 9, the crack growth rates for the events considered are:

HPCI Injection: $\frac{\Delta a}{\Delta n} = 0.0054 \text{ in / cycle}$

Core Spray Injection: $\frac{\Delta a}{\Delta n} = 0.135 \text{ in / cycle}$

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March 3, 1993
AJG-93-015/PCR-93-032

Mr. Peter Kissinger
Northern States Power Company
Monticello Nuclear Generating Plant
2807 West Hwy 75
Monticello, Minnesota 55362-9637

Subject: Third Party Review of General Electric Evaluation Approach for Continued Operation of Gracked Core Spray Pipe at Monticello Nuclear Generating Plant

Dear Pete:

As authorized by you on February 19, 1993, Structural Integrity Associates, Inc. (SI) has performed a third party review of the General Electric Nuclear Energy Division evaluation approach used to justify the technical suitability of the subject core spray piping for continued operation for one cycle at Monticello without repair. The SI review team consisted of Peter Riccardella, Anthony Giannuzzi and Hal Gustin.

Our review consisted of:

1. a review of the of the drawings supplied by you of the tee box and core spray arms in the vicinity of the cracking,

2. a review of the video tape (also supplied by you) illustrating the cracking location and the surrounding vicinity, and

3. participating in a meeting with General Electric personnel to review their approach to the stress and fracture mechanics analyses used to justify continued operation of the core spray system without repair for one additional cycle.

Note that our review concentrated on GE's overall technical approach and methodology, but did not include detailed verification or independent calculations. The following paragraphs describe the results and conclusions of our review.

Based upon the crack location in the heat affected zone of the pipe to tee box weld, as observed in the video tape, plus the tee pipe cover plate weld in the immediate vicinity of this attachment weld, the 304 stainless steel material at the crack location is expected to be highly sensitized and in a state of high welding residual stress. Also recognizing that the BWR coolant in this region is highly oxidizing, both on the OD due to the downcomer flow and on the ID due to stagnant conditions in the core spray line, it is concluded that the most likely failure mechanism is intergranular stress corrosion cracking (IGSCC) of the weld sensitized material. Furthermore, it is highly probable, although not conclusively so, that the cracking initiated on the outside surface of the pipe, as two separate cracks in separate arc strikes observed in the videotape. Weld solidification puddles associated with arc strikes are highly susceptible sources for IGSCC initiation because of additional sensitization, residual stresses and altered physical and mechanical properties. A likely scenario is that two separate cracks initiated in the two arc strikes on the OD of the pipe, and grew towards one another by IGSCC in the direction of greatest primary bending stress and largest expected residual stress.

The GE analytical approach in justifying continued operation of the core spray system without repair for an addition operating cycle included the following stress and fracture mechanics considerations:

1. Membrane and bending stress were determined using an ANSYS beam model due to differential thermal expansion between the piping and vessel, downcomer flow forces, and anticipated pressure drop in the core spray piping and spargers under core spray operation. Calculated stress due to these conditions were quite low (approximately 1.5 ksi membrane plus 1.5 ksi bending).

2. The critical crack length was determined under these loads for a through-wall crack and was found to be on the order of 235° of the circumference.

3. The currently observed crack length is approximately 3.5", or 66° of the circumference, indicating considerable margin to critical crack length (~9"). GE also added approximately 1" to the observed crack length in their evaluation to allow for possible greater length on the inside surface, since ultrasonic examination (UT) of the crack had not yet been performed. (Subsequent UT indicated this to be conservative.)

4. Potential leakage through the crack under core spray operation was estimated, assuming the crack to be a .01" wide slot 180° of circumference, or ~9" long. The resulting leakage flow rate is on the order of 23 gpm, which is negligible compared to overall core spray delivery capacity of 4000 to 6000 gpm. The 23 gpm value seems to be a very conservative estimate for a crack of this length based on SI's independent experience in computing leakage rates through cracks.

5. A conservatively predicted crack growth rate during the next fuel cycle would produce only an additional 1 to 1.5" of crack length. The crack growth rate was assumed to be an upper bound of 4 x 10^{-5} in/hr, independent of stress intensity, during the entire subsequent operating cycle. This estimate of crack growth rate is extremely conservative as illustrated by examining the NRC recommended curve in Figure 1, taken from NUREG-0313, Rev. 2.

Although Structural Integrity Associates personnel have not performed detailed independent check calculations for the above items due to time constraints, the technical approach appears to us to be quite sound and the results appear reasonable, based on our experience with these types of evaluations. In our view, the GE analysis demonstrates a justifiable basis for operation of the core spray system for one additional cycle at Monticello without repair.

If you have any questions regarding our review comments, or if we can be of any further service, please feel free to call. Thank you for this opportunity to be of service to you.

Very truly yours,

A. J. Giannuzzi

P. C. Riccardella

/ms
attachment

cc: B. Day (Northern States Power)

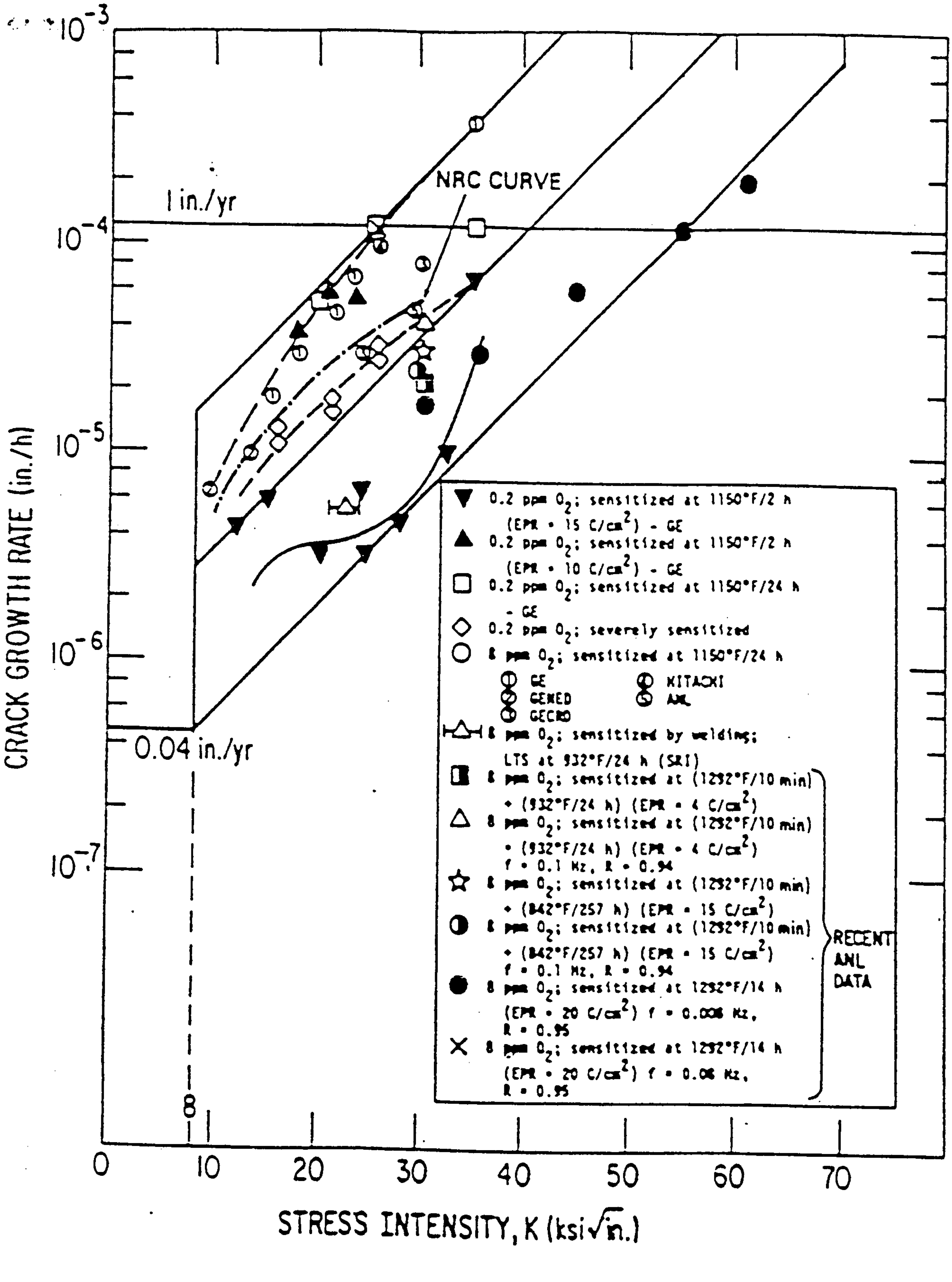

Figure 1
CRACK GROWTH RATE DATA