hcep

GEAP-10205
AEC RESEARCH AND
DEVELOPMENT REPORT
JUNE 1970

MASTER

RECEIVED BY DUE DEC 28 1970

STATUS OF PIPE RUPTURE STUDY AT GENERAL ELECTRIC COMPANY PART III

Compiled by
S. R. VANDENBERG

U. S. ATOMIC ENERGY COMMISSION
CONTRACT AT(04-3)-189
PROJECT AGREEMENT 37

GENERAL ELECTRIC

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

LEGAL NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Atomic Energy Commission, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

GEAP-10205
AEC Research and
Development Report
June 1970

**STATUS OF PIPE RUPTURE STUDY
AT GENERAL ELECTRIC COMPANY
PART III**

Compiled by
S. R. Vandenberg

Approved:

S. R. Vandenberg

S. R. Vandenberg
Project Engineer

Approved:

D. H. Imhoff

D. H. Imhoff, Manager
Development Engineering

Prepared for the
U. S. Atomic Energy Commission
Contract AT(04-3)-189
Project Agreement 37

*Printed in U.S.A. Available from the
Clearing House for Federal Scientific and Technical Information
National Bureau of Standards, U.S. Department of Commerce
Springfield, Virginia
Price: \$3.00 per copy*

ATOMIC POWER EQUIPMENT DEPARTMENT • GENERAL ELECTRIC COMPANY
SAN JOSE, CALIFORNIA 95125

G138-Des. Eng-74
250-ACG-11/70

GENERAL ELECTRIC

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

LEGAL NOTICE

This report was prepared as an account of Government sponsored work. Neither the United States, nor the Commission, nor any person acting on behalf of the Commission:

- A. Makes any warranty or representation, expressed or implied, with respect to the accuracy, completeness, or usefulness of the information contained in this report, or that the use of any information, apparatus, method, or process disclosed in this report may not infringe privately owned rights; or*
- B. Assumes any liabilities with respect to the use of, or for damages resulting from the use of any information, apparatus, method, or process disclosed in this report.*

As used in the above, "person acting on behalf of the Commission" includes any employee or contractor of the Commission, or employee of such contractor, to the extent that such employee or contractor of the Commission, or employee of such contractor prepares, disseminates, or provides access to, any information pursuant to his employment or contract with the Commission, or his employment with such contractor.

TABLE OF CONTENTS

Abstract	1
1 Introduction	1
2 Low Cycle Fatigue	1
2.1 Crack Initiation in the Presence of Stress-Strain Concentrations	1
2.2 Fatigue Crack Growth Rate and Geometry Effects	9
2.3 Fatigue Testing of Prototype Piping	19
2.4 Cyclic Strain Aging of Carbon Steel	37
3 Effect of BWR Environment	43
3.1 Fatigue Machine and Specimen	43
3.2 Dresden I Test Loop	43
3.3 Air Fatigue Test Facility	49
3.4 Reactor Pressure Vessel Materials	49
3.5 Electrochemical Characterization of Coolant	50
4 Fracture Mechanics	52
5 Computer Code Development	54
6 Probability Study	54
6.1 The Distribution of Time to Damage Method	54
6.2 Application to a Piping Study Surface	57
6.3 Preliminary Conclusions	58
6.4 Extensions for System Reliability Estimate	61
References	63
Distribution	65

LIST OF ILLUSTRATIONS

Figure	Title	Page
2-1	Notched Test Specimens	4
2-2	Smooth Specimen Strain-Controlled Test Results	5
2-3	Nominal Stress Range versus Cyclic Life for Smooth and Notched Specimens of Type-304 Stainless Steel Tested at Room Temperature	6
2-4	Comparison of Room Temperature and 550°F Cyclic Life for Grooved Cylinder Specimens, $K_t = 3.3$	7
2-5	Comparison of Smooth and Notched Cyclic Life Based on Measured Strain Ranges	9
2-6	Correlation of Crack Growth Rate and Stress Intensity Factor, ΔK , for A-212B Carbon Steel at Various Temperatures	11
2-7	Crack Growth Curves for Various Notch Configurations in Type-304 Stainless Steel at Room Temperature, at a Gross Tensile Stress of 43,000 psi	12
2-8	Crack Growth Correlation for Specimens of Widely Varying Crack Geometries	13
2-9	Crack Growth Correlation for Pipe and Plate Specimens of A-106B Carbon Steel Pipe at Room Temperature (Plate Specimens Prepared from Pipe and Re-Heat-Treated)	15
2-10	Crack Growth Correlation for Welded and Unwelded Pipe Specimens of A-106B and Type 304	16
2-11	Crack Growth in A-212B and 2-1/4 Cr-1 Mo under Conditions of Controlled Plastic Strain for Tension and Bending Cycling	17
2-12	Crack Growth Correlation for 2-1/4 Cr-1 Mo Alloy with Various Loading Schemes for Both Tension and Bending	18
2-13	2-1/4 Cr-1 Mo Alloy Steel, Room Temperature, Stress Range versus Crack Initiation Cycles	24
2-14	Carbon Steel, Room Temperature, Stress Range versus Crack Initiation Cycles	25
2-15	Type-304 Stainless Steel, Room Temperature, Stress Range versus Crack Initiation Cycles	26
2-16	A-106B Carbon Steel, 550°F, Stress Range versus Crack Initiation Cycles	27
2-17	Type-304 Stainless Steel, 550°F, Stress Range versus Crack Initiation Cycles	28
2-18	Carbon Steel, Room Temperature, Through-Wall Crack Growth Rate versus ΔK	29
2-19	Stainless Steel, Room Temperature, Through-Wall Crack Growth Rate versus ΔK	30
2-20	Stainless Steel, 550°F, Through-Wall Crack Growth Rate da/dN versus ΔK	31
2-21	A-106B Carbon Steel, Room Temperature, Circumferential Crack Growth Rate versus Stress Intensity Range	32
2-22	Type-304 Stainless Steel, Room Temperature, Circumferential Crack Growth Rate versus Stress Intensity Range	33

LIST OF ILLUSTRATIONS (Continued)

2-23	2-1/4 Cr-1 Mo, Room Temperature, Circumferential Crack Growth Rate versus ΔK Correlation	34
2-24	A-106B Carbon Steel, 550°F, Circumferential Crack Growth Rate versus Stress Intensity Range	35
2-25	Type-304 Stainless Steel, 550°F, Circumferential Crack Growth Rate versus Stress Intensity Range	36
2-26	Effect of Test Temperature on Ductility and Strength of A-212B Steel Tested in Tension at Various Strain Rates	38
2-27	Typical Charpy Impact Curves	39
2-28	Effect of Test Temperature on Charpy Impact and Static Tensile Properties	40
2-29	Effect of Cyclic Strain at Various Number of Cycles to Shift the Charpy 50% FATT (Reference Condition 50% FATT = 0°F)	41
2-30	Notch Bend Strength at Fracture as a Function of Temperature for Pre-Cyclically Strain-Aged Specimens	42
3-1	Fatigue Test Machine for Materials Test Loop at Dresden I Nuclear Power Station	44
3-2	Completed Low-Cycle Fatigue Unit Prior to Placement in its Autoclave	44a
3-3	Low-Cycle Fatigue Specimen	45
3-4	Type-304 Stainless Steel, Low-Cycle Fatigue versus Cycles to Crack	46
3-5	Schematic Diagram of Materials Testing Loop at Dresden I	47
3-6	Electrochemical Flange Assembly	51
4-1	Failure Diagram for A-106B Pipe with Various Flaws	53
6-1	Distribution of Time to Damage Method	55
6-2	Piping Probability Diagram for One Pipe Study Surface and Damage Mechanism	56
6-3	Probability of Leakage	57
6-4	Uncertainty Distributions for Log C, B, a_0/t Max, and Stress	59
6-5	Judgmental Uncertainty Distribution for a_0/t , Initial Crack Depth Ratio	61
6-6	Monte Carlo Using Importance Sampling to Estimate $\Pr[X \leq x_0]$	62

ABSTRACT

The Pipe Rupture Study is designed to extend the understanding of failure-causing mechanisms, and to provide improved capability for evaluating reactor piping systems to minimize the probability of failures. Following a detailed review to determine the effort most needed to improve nuclear system piping (Phase I), analytical and experimental efforts (Phase II) were started in 1965. This status report summarizes the accomplishments of a broad program in (a) fatigue studies through both bench-scale and large-component tests, (b) at-reactor tests of the effect of primary coolant environment on the fatigue behavior of piping steels, (c) fracture mechanics, (d) solution of problems in stress analysis, and (e) failure probability study of actual reactor piping systems.

1. INTRODUCTION

This is the fourth status report of the Pipe Rupture Study. The initial article,¹ covering Phase I described the results of surveys of piping failures, design practices, potential failure mechanisms, and proposed courses of action to improve reliability. The second² and third³ articles reported progress after 1 and 3 years, respectively. The present report summarizes, after 5 years, the accomplishments on the following tasks:

- Fatigue
- Effect of BWR Environment
- Fracture Mechanics
- Stress Analysis
- Probability Study

The Pipe Rupture Study is intended to improve the reliability of BWR and PWR primary piping. Major portions of the work have been completed in the Fatigue and Fracture Mechanics Tasks. Continuing are the studies of Probability, Prototype Pipe Fatigue, and Environmental Effects on Materials. Quarterly and topical reports are available for detailed coverage.⁴

Although full-severance breaks in piping systems are very rare, damage mechanisms exist which, if undetected, could conceivably lead to such incidents. Since such failures are potentially very serious from the standpoint of safety, they must be kept to a minimum. Toward this end, more complete data on materials behavior are essential. The damage mechanisms emphasized in the program are low cycle fatigue, cyclic strain aging, and cyclic-stress-assisted corrosion cracking. Throughout these studies, clear delineation is made of cycles and conditions to initiate cracking, crack growth rate, and the number of cycles at failure; i.e., at the attainment of critical crack size. The materials of boiling water and pressurized water systems are being studied, including plain carbon steel, 2-1/4% chromium-1% molybdenum steel, Types-304 and -304L stainless steel, and Inconel-600.

2. LOW CYCLE FATIGUE

2.1 CRACK INITIATION IN THE PRESENCE OF STRESS-STRAIN CONCENTRATIONS (E. Krempl and S. Yukawa)

The objective of this task was to develop an understanding of crack initiation in notched members under low-cycle fatigue loading, to the extent that the results could be used in engineering analysis. The work was completed, and a final report was prepared summarizing the experimental data and an analysis of cyclic life calculation procedures.⁵ The following paragraphs contain an outline of the scope of the experimental work, and the major findings are discussed.

Cyclic loading tests were conducted on smooth and notched specimens, using materials representative of nuclear piping: carbon steel, 2-1/4 Cr-1 Mo alloy steel, and Type-304 stainless steel, all in the annealed condition. Testing was done at room temperature and at 550°F. The smooth specimens were axially loaded and cycled in the completely reversed,

strain-controlled mode. The notched test specimens are shown in Figure 2-1; of these, the flat plate and the grooved cylinder specimens were cycled under axial loading and the notched bar in cantilever bending. The various combinations of materials, notch geometries, loading conditions, and temperatures investigated are summarized in Table 2-1.

The smooth specimen total strain range versus cyclic life data for the three materials at the two temperatures is presented in Figure 2-2. At room temperature the three materials exhibit essentially identical behavior in this test. The 2-1/4 Cr-1 Mo steel shows virtually no difference between the 550°F and the room temperature results. The carbon steel appears to have a somewhat shorter cyclic life at 550°F compared with room temperature at high cyclic strain, and the Type-304 stainless steel has a moderately longer life at 550°F at the lower cyclic strains.

All three materials were cyclically strain-hardened at both test temperatures. In the case of the 2-1/4 Cr-1 Mo steel and the Type-304 stainless steel, the room temperature and the 550°F cyclic stress-strain curves were essentially the same for each material. In contrast, the carbon steel cyclically hardened to a much greater extent at 550°F than at room temperature.

In the notched specimen studies, primary emphasis was on test conditions resulting in cyclic lives in the 100- to 10,000-cycle range; therefore, the nominal values of the applied cyclic stresses ranged from somewhat below to considerably above the conventional yield strengths. Several kinds of information were obtained to serve as a basis for analysis and correlation. Cyclic life was determined for crack initiation, N_C (defined as the presence of an approximately 0.01-inch-long crack), and for total life, N_f (complete separation). In the analysis, emphasis was placed on N_C since it is believed to be a more meaningful basis. In addition, the local strains in the notch root were measured on several specimens, using small strain gages. These strain measurements were continued into the cycling regime to the capability extent of the gages.

Two of the several factors that may influence notched cyclic life are the state of stress in the notch region, and the cycling mode. An insight into the stress-state effect was obtained from the notched-flat-plate and the grooved-cylinder specimen series. The relatively thin plate specimens should approach a plane stress condition in the notch region while the grooved cylinders approach a plane strain condition. The cycling mode effect (zero-tension versus tension-compression) was studied in the grooved cylinder series; this effect can also be considered as a mean stress effect.

The most complete set of results for evaluating the effects of these two factors was obtained in the room temperature tests for a K_t value of about 3. These results can be summarized for the three materials by the following:

- a. The notched plate and the cylinder specimens had essentially identical cyclic lives (both N_C and N_f) under zero-tension cycling.
- b. For equal values in the range of cyclic stress, crack initiation occurred earlier under reversed tension-compression cycling relative to zero-tension; however, the total cyclic life was longer for the reversed case.

These results indicate that stress and strain reversals are more important than stress-state effects on notched low-cycle fatigue behavior. The notch root strain measurements support this conclusion. In the zero-tension case, the local cyclic strains are nearly elastic after the initial cycle plastic behavior, whereas in the reversed loading case, the cyclic strains continue to have an elasto-plastic hysteresis behavior.

The effect of varying the K_t values and the type of loading is illustrated in Figure 2-3 for the Type-304 stainless steel material; the effect shown is representative of the results found for the other two materials. This figure includes the smooth (unnotched) results for comparison; the stress range in this case is the stabilized value obtained in strain-controlled cyclic tests. As expected, for a specific type of notched specimen and type of loading, increasing K_t leads to a decrease in cyclic life to crack initiation. However, at similar values of K_t , the reversed bending results are quite different from the axial push-pull results. A part of this difference lies in the use of nominal stresses for the comparison. The bending results shown are based on nominal stress range values calculated from the applied bending moment and the simple beam equation, using the net section of the notched area. Clearly, under elasto-plastic conditions, a bending nominal stress calculated in this way has no

Table 2-1
PARAMETERS INVESTIGATED

Material	Room Temperature							550° F	
	Grooved Cylinders				Flat Plates		Notched Bar	Grooved Cylinders	Flat Plates
	$K_t=1.3$	Completely Reversed $K_t=1.9$	$K_t=2.5$	$K_t=3.3$	Zero to Tension $K_t=2.9$	Zero to Tension $K_t=3$	Bending Completely Reversed $K_t=1.8$	Completely Reversed $K_t=3.3$	Zero to Tension $K_t=3$
Carbon Steel	—	X	—	X	X	X	—	X	X
2-1/4 Cr—1 Mo Alloy Steel	X	X	X	X	X	X	X	X	X
304 Stainless Steel	—	X	X	X	X	X	X	X	X

NOTE: X in table means that this parameter was studied.

All tests under load control.

K_t = uniaxial, theoretical, elastic stress concentration factor.

FLAT PLATE

GROOVED CYLINDER

NOTCHED BAR

Figure 2-1. Notched Test Specimens

Figure 2-2. Smooth Specimen Strain-Controlled Test Results

Figure 2-3. Nominal Stress Range versus Cyclic Life for Smooth and Notched Specimens of Type-304 Stainless Steel Tested at Room Temperature

direct relationship to a tensile nominal stress based on the tensile applied load and the net section area used for the axially loaded specimens. In the analytical work performed, it was shown that an approximate elasto-plastic stress analysis using the cyclic stress-strain data can partially correct the inadequacies of the simple nominal stress method.

The notched cyclic life behavior at 550°F was found to be similar to or different from the room-temperature results, depending on the material. The results for the notched flat plate specimens (zero-tension cycling) show:

- a. For the 2-1/4 Cr-1 Mo steel, the 550°F and the room-temperature cyclic lives (both N_c and N_f) were essentially identical.
- b. For the carbon steel and the Type-304 stainless steel, the N_c and N_f values at 550°F were less than the room temperature values at high cyclic stresses, but were slightly greater at the lowest cyclic stress investigated.

For the grooved cylinder specimens, a comparison of the cyclic life to crack initiation at the two test temperatures is shown in Figure 2-4. The 2-1/4 Cr-1 Mo steel again exhibits identical behavior at the two temperatures. The Type-304 stainless steel has substantially lower cyclic life at 550°F, while the carbon steel shows a slight-to-moderately longer life at 550°F compared with that at room temperature.

Figure 2-4. Comparison of Room Temperature and 550° F Cyclic Life for Grooved Cylinder Specimens, $K_t = 3.3$

These results demonstrated that temperature effects on notched low-cycle fatigue behavior are not directly or even qualitatively predictable from smooth-specimen strain range versus cyclic life results alone. The carbon steel and the Type-304 stainless steel are examples where reversal in temperature effects were noted between smooth and notched behavior.

As mentioned earlier, local notch root strain measurements were made on several notched test specimens. The results generally followed the elasto-plastic behavior to be expected at stress concentrations:

- a. A rapid increase in local strains with increasing nominal stress upon exceeding the local elastic behavior conditions.
- b. Higher K_t values produce higher elastic and plastic notch strains for a given nominal stress.
- c. A decrease in local notch strains with cycling under load-controlled conditions, particularly at higher nominal cyclic stress values; at low cyclic stresses the local strains showed little or no cyclic changes.

The third feature, above, would be expected from the fact that the three materials used were all of the cyclic strain-hardening variety. Cyclic softening materials may exhibit an opposite behavior.

The measured notch strains were compared with values predicted by the Stowell method of calculating elasto-plastic stresses and strains at stress concentrations. By using the smooth specimen cyclic stress-strain curve as a basis for the calculation, moderate to good agreement between calculated and measured values was obtained for the low K_t and high K_t series (1.3 and 3.3). However, the agreement was poor at intermediate K_t 's of 1.9 and 2.5; the calculated values were high. This indicates a clear need for the development of more sophisticated methods for the analysis of these complex cyclic elasto-plastic stress/strain behaviors.

The measured notch strains can be used directly to compare notched and smooth specimen cyclic lives. Such a comparison is shown in Figure 2-5 for the 2-1/4 Cr-1 Mo steel results. Note that most of the notched values lie to the lower cyclic life side of the smooth-specimen behavior. This feature was also true for the other two materials studied. In the figure, the notched data shown are the notch strain range, for the initial cycles, which would be displaced to slightly lower strain values if strains after a period of cycling were used.

Note also in Figure 2-5 that the bending notched cyclic results are similar to the push-pull results on this basis of comparison, in contrast to the simple nominal stress basis comparison discussed earlier.

As experimental limitations precluded any notch strain measurements on the 550°F test specimens, a direct comparison on a measured strain basis of the temperature effects was not possible.

The experimental data obtained were used to assess the several procedures available for the engineering analysis of low-cycle fatigue in the presence of stress concentrations. From this assessment, it was judged that a procedure based on the so-called "stress approach" appears to be the most useful for current design analysis usage. In essence, this procedure involves the following steps:

- a. For the cyclic nominal stress and K_t values of interest, determine the *plastic stress* concentration factor. The Stowell method can be used; or for a conservative evaluation, a plastic stress concentration factor equal to K_t should be used. In the case of bending loading, the nominal stress would be the fictitious value derived from a simple elastic stress analysis.
- b. Multiply the nominal stress by the plastic stress concentration factor to obtain the local cyclic stress.

Figure 2-5. Comparison of Smooth and Notched Cyclic Life Based on Measured Strain Ranges

- c. Convert the local cyclic stress to a corresponding local cyclic strain by using the cyclic stress-strain information.
- d. Enter the strain value from Step c. into the strain-versus-cyclic-life curve determined from smooth-specimen tests, to obtain cyclic life to crack initiation for the notched member.

This procedure gave results that were generally conservative compared with the experimental observations and therefore provides a reasonable approach for design purposes.

2.2 FATIGUE CRACK GROWTH RATE AND GEOMETRY EFFECTS (A. J. Brothers and S. Yukawa)

The primary objective was to determine the feasibility of rationally designing against cyclic crack growth in piping systems. The approach is based on correlating the measured crack growth rates with either stress or strain-field parameters for stress and strain-controlled loading conditions, respectively. For cycling conditions under nominally stress-controlled conditions, growth rates are correlated with an analytically derived stress field parameter, K —the stress intensity factor—which in a single quantity expresses the influence of cyclic stress level, crack length, and configuration. Conceptually, if the crack growth rates are determined for a simple test configuration for which an expression for K exists, it is possible to estimate the rate of growth of cracks in other geometries (pipe, for example) for which an analysis for K is available. For cycling under strain-controlled conditions, growth rates are correlated with the total cyclic strain range; or where loads are known, with the calculated stress intensity factor.

To implement this approach, fatigue crack growth rates were measured for A-212B carbon steel, 2-1/4 Cr-1 Mo, and Type-304 steels representative of those alloys used for nuclear piping.⁶ The variables considered were those believed to

generally influence crack growth rates; i.e., temperature, cyclic frequency, mean stress level, tension versus bend loading, and load versus strain-controlled loading. In addition to plate specimens, tests have been made of both welded and unwelded pipe sections using a semi cylindrical pipe specimen. General conclusions and salient features are reported here.

- a. Cyclic crack growth rates in these steels at all test temperatures, and for all conditions of cycling, can be correlated with the concepts of Griffith-Irwin fracture mechanics as proposed by Paris.⁷
- b. Cyclic crack growth rates for 2-1/4 Cr-1 Mo and Type-304 steels are not significantly affected by temperature in the range of room temperature to 600°F. Growth rates in carbon steel at about 500°F are three to five times greater than those measured at room temperature. Although there is a significant effect of cyclic frequency on total cyclic life in carbon steel at 600°F, the results suggest that this arises primarily from an effect on cycles for crack initiation rather than growth.
- c. For the narrow range examined in the carbon and 2-1/4 Cr-1 Mo steel, there is a significant effect of superimposed mean stress on the alternating stress intensity factor range required to obtain a given growth rate; for Type-304 steel, for the range examined, there is no apparent effect of mean stress.
- d. Crack growth rates for commercially available A-106B carbon steel and Type-304 piping steels tested in zero-to-tension loading at room temperature, using semicylindrical pipe sections, exhibit identical crack growth rates in both welded and nonwelded conditions.
- e. Crack growth rates for the three steels for axial and bending tension-compression over controlled strain ranges suggest a simple exponential relationship between growth rate and total strain range. However, growth rates for part-through surface cracks (for axial and bending reversed loading and a given strain range) are much less than those for through-cracked plates, and not simply determinable from strain range. A scheme based on fracture mechanics concepts has been proposed for rationally correlating geometry effects under strain-controlled cycling conditions.⁸

A typical correlation of cyclic crack growth rate and stress intensity factor is shown in Figure 2-6 for carbon steel for various test temperatures. Within nominal data scatter, these results verify a simple power law relationship which successfully correlates crack growth rates at various stress levels and crack lengths. The results shown in Figure 2-6 were obtained by using zero-to-tension loading in centrally-slotted plates, with gross section stresses ranging from 0.8 to 1.3 times the yield strength; the derived stress intensity factor, K , for this particular geometry is equal to:

$$K = \sigma_g [W \tan \pi a/W]^{1/2} \text{ (Ref. 9)}$$

where

- σ_g = Gross section alternating stress range, psi,
- W = Plate width, inches, and
- a = Half-slot plus crack length, inches.

Since the stress intensity factor incorporates effects of the part or specimen geometry, it should (ideally) be suitable for correlating growth rates in different configurations for which analyses for K are available. To test this concept, crack growth rate tests were performed on specimens containing part-through semi-elliptical surface slots tested in both tension and bending. Typical crack growth curves shown in Figure 2-7 indicate that for identical stresses there is a significant effect of geometry both on total life and crack growth rates. However, on the basis of the *stress intensity factor* (Figure 2-8), the rates for part-through surface slots are, for equal ΔK ranges, essentially identical to those measured for wide, through-cracked plates.

Figure 2-6. Correlation of Crack Growth Rate and Stress Intensity Factor, ΔK , for A-212B Carbon Steel at Various Temperatures.

Figure 2-7. Crack Growth Curves for Various Notch Configurations in Type-304 Stainless Steel at Room Temperature, at a Gross Tensile Stress of 43,000 psi

Figure 2-8. Crack Growth Correlation for Specimens of Widely Varying Crack Geometries

To compare crack growth rates in commercially available pipe and plate materials, both welded and nonwelded semicylindrical pipe sections were tested in zero-to-tension loading. Initial comparison revealed apparently greater growth rates in the pipe sections; however, as shown in Figure 2-9, for tests of A-106B piping and plates prepared from identical materials there is a significant effect of specimen shape. Correction for the effect of the semicircular geometry results in crack growth rates similar to those for the plate specimens. As shown in Figure 2-10, there appeared to be no significant effect of welding on the crack growth correlation of either Type-304 or A-106 piping.

For cycling conditions involving stresses and/or strains exceeding general yield conditions, tests have been performed using both through-cracked tension plates and part-through cracked bend specimens. These results, summarized in Figure 2-11, suggest a simple exponential dependence of crack growth rate on total strain range for both cases. However, the general level of growth rates differs widely for the two testing procedures, reflecting primarily the different geometries in the two tests. A method based on fracture mechanics concepts has been tentatively proposed to correlate part-through and through-crack geometries.⁸

Since stress intensity factors are derived from linear elastic stress analysis, satisfactory correlations for plastically strain-cycled specimens perhaps might not be expected. However, as shown in the overall crack growth correlations for the 2-1/4 Cr-1 Mo steel (Figure 2-12), the stress intensity factor range was successfully correlated with crack growth rate for all conditions evaluated in this program, including strain-cycled tensile and bend specimens.

Figure 2-9. Crack Growth Correlation for Pipe and Plate Specimens of A-106B Carbon Steel Pipe at Room Temperature (Plate Specimens Prepared from Pipe and Re-Heat-Treated)

Figure 2-10. Crack Growth Correlation for Welded and Unwelded Pipe Specimens of A-106B and Type 304

Figure 2-11. Crack Growth in A-212B and 2-1/4 Cr-1 Mo under Conditions of Controlled Plastic Strain for Tension and Bending Cycling

Figure 2-12. Crack Growth Correlation for 2-1/4 Cr-1 Mo Alloy with Various Loading Schemes for Both Tension and Bending

2.3 FATIGUE TESTING OF PROTOTYPE PIPING

(E. Kiss and J. D. Heald)

The objectives of the prototype pipe fatigue testing program¹⁰ are: (a) to investigate crack initiation and growth with loadings representative of actual service, (b) to determine whether critical crack sizes are attained, and (c) to assess the validity of crack initiation and growth rate predictions derived from bench-scale tests of plates and bars. Forty-nine large pipe assemblies have been tested, including the following:

- a. **Pipe Straights (Notched)**
 - Room Temperature 23 specimens
 - 550°F 7 specimens
- b. **Welded Pipe Straights (Unnotched)**
 - 550°F 5 specimens
- c. **Elbows**
 - Room Temperature 5 specimens
 - 550°F 4 specimens
- d. **Tees**
 - Room Temperature 5 specimens

The results of these tests are summarized in Table 2-2. Cyclic bending, plus a constant pressure of 1000 psig, was applied in all room-temperature tests. Cyclic bending with no internal pressure was applied in the 550°F tests, with the exception of one stainless steel, straight pipe section, HSNP 80-1, which was pressurized at 1000 psia. Probably the most striking experimental observation of this program is the large size of circumferential fatigue cracks which, in no case, led to pipe severance. Even in carbon steel, a crack 78% of the circumference did not run.

Additional tests are planned at PWR pressure conditions, pipe straights notched circumferentially at room temperature; and at PWR and BWR pressures, specimens flawed axially.

2.3.1 Comparison with Bench-Scale Test Results

The crack initiation results for the piping materials tested are compared, in Figures 2-13 through 2-17, with those obtained in a laboratory tensile machine⁵ applying fully-reversed loading to grooved cylinders (with $K_t = 3.0$). The notches in the pipe specimens also have a $K_t = 3.0$. The notched pipe specimen data show good agreement with bench-scale tests. Crack initiation data points for ells and tees are also shown in the above figures, but quantitative comparison should not be made since the geometrical K_t for the ells and tees is different, i.e., not equal to 3.0.

Crack growth through pipe walls is compared with test results⁶ obtained from flat-plate specimens in Figures 2-18 through 2-20. The slopes of the prototype pipe data appear to be less than the slopes of the laboratory data. Both types of data agree very well in the region of ΔK between 40 and 70 $\text{ksi}\sqrt{\text{in}}$. Above this region, the laboratory data, if used to predict crack growth rates in piping, would be conservative but below it, such predictions would not be conservative.

A comparison of circumferential crack growth data as shown in Figures 2-21 through 2-25, reveals better agreement than the through-wall case. The slope of the data points appears to be the same, and the only deviation is probably due to experimental scatter.

Table 2-2
TEST PARAMETERS

Sample No.	Schedule	Notch*	Nominal Stress Range	N_I (Cycles to Crack Initiation)	N_W (a)	N_ϕ (b)
Room Temperature—Carbon Steel						
<u>NOTCHED PIPING</u>						
CSS-3	40	I	72,800	507	2,109 (578)	2,109
CSS-4	40	II	66,100	~1,300	8,572 (3,272)	28,096 (19,524)
CSS-5	40	I	52,800	9,600	13,398 (3,798)	17,186 (3,688)
CSS-6	40	II	77,000	517	1,759 (936)	2,304 (546)
CSS-7	80	I	73,200	550	2,289 (752)	2,784 (495)
CSS-8	80	II	48,800	7,500	14,526 (4,581)	14,526
CSS-9	80	I	45,000	17,700	34,811 (16,726)	34,811
CSS-10	80	II	40,200	9,000	16,005 (6,005)	16,005
CSS-11	160	I	89,000	260	1,660 (693)	1,660
CSS-13	160	I	64,000	1,050	17,400 (12,550)	36,009 (16,809)
CSS-107	80	I	74,100	200	4,575 (3,375)	14,205 (9,130)
CSS-109	80	I	72,300	600	6,000 (3,400)	16,850 (7,611)
<u>ELBOWS</u>						
CCLS-1	40	None	43,800	< 900	1,176	8,000 (6,824)
CCLS-2	40	None	42,600	< 3,249	7,899	13,599 (5,700)

(a) N_W = Total cycles when crack penetrated through wall (includes N_I). Number in parentheses is full amplitude cycles included in $N_W - N_I$.

(b) N_ϕ = Total cycles after circumferential crack has grown to length where test terminated (includes N_W and N_I). Number in parentheses is full amplitude cycles in $N_\phi - N_W$.

* II = Inside Notch
I = Outside Notch

Table 2-2 (Continued)

Sample No.	Schedule	Notch	Nominal Stress Range	N_i (Cycles to Crack Initiation)	N_w (a)	N_ϕ (b)
Room Temperature—Carbon Steel						
<u>TEES</u>						
CCTS-1	40	None	68,500	9,800	21,079	30,700 (9,621)
CCTS-2	40	None	68,300	6,100	9,367	10,449 (1,082)
Room Temperature—Stainless Steel						
<u>NOTCHED PIPING</u>						
CSS-15	40	I	48,300	4,500	24,630 (15,627)	75,796 (51,166)
CSS-16	40	II	59,600	1,400	8,596 (7,196)	42,404 (33,908)
CSS-23	160	I	82,000	165-189	2,350 (1,558)	3,255 (905)
CSS-25	160	I	70,460	500	16,250 (11,383)	30,629 (14,379)
CSS-119	80	I	74,600	425	5,156 (4,231)	20,603 (15,047)
<u>ELBOWS</u>						
CSLS-1	40	None	44,200	< 907	907	4,202 (3,295)
CSLS-2	40	None	42,200	< 5,025	6,838	11,096 (4,258)
CSLS-3	40	None	44,200	< 1,500	4,469	13,902 (9,433)
<u>TEE SPECIMENS</u>						
CSTS-1	40	None	68,700	< 4,575	4,575	10,700 (6,126)
CSTS-2	40	None	67,800	< 3,310	3,310	8,000 (4,690)
CSTS-3	40	None	68,770	2,600	3,675	5,604 (1,929)

Table 2-2 (Continued)

Sample No.	Schedule	Notch	Nominal Stress Range	N_i (Cycles to Crack Initiation)	N_w (a)	N_ϕ (b)
Room Temperature—Alloy Steel						
<u>NOTCHED PIPING</u>						
CSS-27	40	I	68,300	330	4,500 (1,170)	20,300 (15,800)
CSS-29	40	I	52,100	3,100	18,950 (8,850)	48,738 (29,788)
CSS-31	80	I	75,300	475	6,800 (4,325)	19,000 (9,100)
CSS-35	160	I	47,500	8,500-9,004	51,400 (31,400)	54,536 (2,636)
CSS-36	160	II	46,600	5,000-7,000	23,570 (13,070)	26,162 (2,593)
CSS-37	160	I	91,300	226	5,171 (2,139)	19,661 (4,951)
550°F—Carbon Steel						
<u>NOTCHED PIPING</u>						
HCN-40-1	40	I	52,700	13,250	26,000 (6,748)	137,000 (109,000)
HCN-40-2	40	I	75,700	925	10,520 (5,595)	21,593 (11,073)
HCN-40-3	40	I	53,000	6,200	51,337 (39,037)	137,405 (85,068)
HCN-40-4	40	I	77,100	730	5,800 (3,070)	13,527 (6,727)
<u>PIPE BUTT WELDS</u>						
HW-1	40	None	58,200	25,000	36,240 (10,740)	38,800 (2,260)
HWD-1	40	None	59,200	< 15,100	15,100 (13,600)	34,905 (19,305)
<u>ELBOWS</u>						
HCLS-1	40	None	43,500	< 700	760	3,449 (2,609)
HCLS-2	40	None	43,100	< 3,299	26,100	31,100 (5,000)

Table 2-2 (Continued)

Sample No.	Schedule	Notch	Nominal Stress Range	N _i (Cycles to Crack Initiation)	N _w ^(a)	N _φ ^(b)
550°F—Stainless Steel						
<u>NOTCHED PIPING</u>						
HSN-40-S*	40	I	57,200	730	8,702 (3,971)	41,304 (31,851)
HSN-40-1	40	I	58,400	800	5,250 (3,550)	20,500 (14,750)
HSNP-80-1**	80	II	59,500	≈800	6,900 (4,100)	7,100 (200)
<u>PIPE BUTT WELDS</u>						
HW-3	40	None	58,700	3,000-3,500		
HW-3A	40	None	61,100	5,725	7,950 (1,225)	9,800 (1,350)
HWD-3	40	None	59,900	2,049	3,600 (551)	9,700 (5,600)
<u>ELBOWS</u>						
HSLs-1	40	None	28,000	<2,200	2,200	3,550 (1,350)
HSLs-2	40	None	42,200	1,799	1,870 (71)	2,089 (219)

* Sensitized—1140°F for 24 hours, air cooled.

** Pressurized to 1000 psi with demineralized water. This was the only component pressurized at 550°F.

Figure 2-13. 2-1/4 Cr-1 Mo Alloy Steel, Room Temperature, Stress Range versus Crack Initiation Cycles

Figure 2-14. Carbon Steel, Room Temperature, Stress Range versus Crack Initiation Cycles

Figure 2-15. Type-304 Stainless Steel, Room Temperature, Stress Range versus Crack Initiation Cycles.

Figure 2-16. A-106B Carbon Steel, 550°F, Stress Range versus Crack Initiation Cycles

Figure 2-17. Type-304 Stainless Steel, 550°F, Stress Range versus Crack Initiation Cycles

Figure 2-18. Carbon Steel, Room Temperature, Through-Wall Crack Growth Rate versus ΔK

THROUGH-WALL CRACK GROWTH RATE, da/dN VERSUS ΔK , STAINLESS STEEL, 550°F

Figure 2-20. Stainless Steel, 550°F, Through-Wall Crack Growth Rate da/dN versus ΔK

Figure 2-21. A-106B Carbon Steel, Room Temperature, Circumferential Crack Growth Rate versus Stress Intensity Range

Figure 2-22. Type-304 Stainless Steel, Room Temperature, Circumferential Crack Growth Rate versus Stress Intensity Range

Figure 2-23. 2-1/4 Cr-1 Mo, Room Temperature, Circumferential Crack Growth Rate versus ΔK Correlation

Figure 2-24. A-106B Carbon Steel, 550°F, Circumferential Crack Growth Rate versus Stress Intensity Range

2.4 CYCLIC STRAIN AGING OF CARBON STEEL

(A. J. Brothers)

Cyclic straining of carbon steels in the strain-aging temperature range, 300–500°F, results in reduced toughness, as noted by an increase in the brittle-to-ductile transition temperature of the Charpy impact test. To determine the possible influence of cyclic strain reversals at elevated temperatures on the impact properties of carbon steel piping materials, tests have been conducted to explore the influence of strain range, number of cycles, and test temperature on the Charpy impact properties of carbon steel.¹¹ Metallurgical effects associated with these changes were investigated by electron microscopy.

Strain aging affects the uniaxial tensile properties, as illustrated in Figure 2-26. This figure shows the characteristic increase in strength, and decrease in ductility, at temperatures in the range of 350 to 400°F. To determine this effect on Charpy impact properties following cyclic deformation in the strain-aging temperature range, plate specimens of 1-in.-thick, 0.24% carbon steel (A-212B) were cycled over various strain ranges and cycles from room temperature to 550°F.

Typical results are shown in Figures 2-27, 2-28, and 2-29. Shown in Figure 2-27 is the shift of the Charpy impact curve to higher temperatures following cycles at various temperatures, and appreciable changes associated with cyclic strain aging are revealed. Exposure at a series of temperatures for a constant number of cycles (Figure 2-28) indicates an effect on the Charpy properties which parallels that observed in the tensile tests. There is the suggestion of a maximum embrittlement at about 350°F, corresponding to the temperature range of maximum effects in the tensile test. At 550°F, a lesser effect is noted; i.e., a change in transition temperature (FATT) of 80°F compared to 100°F at 350°F for equivalent cyclic straining.

The influence of strain range and/or number of cycles for a constant temperature is shown in Figure 2-29. The extent of embrittlement increases as a function of both increasing strain and number of cycles, at least over the range examined. The maximum increase of the 50% FATT was 115°F, observed following 151 cycles at 350°F at a strain range of 2.0%. This figure also illustrates the significantly lower extent of embrittlement associated with static deformation.

Since there is a significant effect of cyclic strain aging on the transition temperature, it was considered likely that under some conditions loss of toughness might result from strain and/or temperature effects at local regions near discontinuities. Consequently, notch-bend tests of 1 inch thick by 8 inches long, precyclically, strain-aged bend bars were conducted over the temperature range of room temperature to -320°F. Precyclic strain aging was performed at conditions designed to effect a local embrittlement corresponding to a shift in 50% FATT of 100°F. The results of these tests (Figure 2-30) reveal no influence of local strain aging to reduce the notch bend fracture strength to levels below that of virgin and fatigue-cracked bars. Since this effect differs from that observed previously by others and could presumably reflect the specific depth of strain-aging, local hardness change, notch depth, or specimen thickness, it is suggested that caution be used in applying these data.

Metallurgical examination of tensile and impact specimens by thin-film transmission studies, electron microscopic examination of fracture surfaces, and standard metallography revealed no evidence of metallurgical precipitation associated with strain aging. However, there were obvious rearrangements of dislocation structure, including the formation of a well-defined cellular structure. From these studies it was not possible to relate uniquely the change in impact properties with fine-scale metallurgical structure.

Figure 2-26. Effect of Test Temperature on Ductility and Strength of A-212B Steel Tested in Tension at Various Strain Rates

Figure 2-27. Typical Charpy Impact Curves

Figure 2-28. Effect of Test Temperature on Charpy Impact and Static Tensile Properties

Figure 2-29. Effect of Cyclic Strain at Various Number of Cycles to Shift the Charpy 50% FATT
(Reference Condition 50% FATT = 0°F)

Figure 2-30. Notch Bend Strength at Fracture as a Function of Temperature for Pre-Cyclically Strain-Aged Specimens

3. EFFECT OF BWR ENVIRONMENT

(E. Kiss and D. A. Hale)

The purpose of this task¹ is to measure the low-cycle fatigue properties of piping steels in BWR systems to determine whether the environment can significantly reduce fatigue life when compared with data obtained in air. The materials are Type-304 stainless steel, Type-304L stainless steel, Inconel-600, and plain carbon steel, in the conditions that occur in service such as: as-received (annealed and pickled); annealed, pickled, and sensitized; welded; and, for the carbon steel, welded and stress-relieved. The strain ranges are 1.0, 0.75 and 0.5%, and the number of cycles in 2 years will be approximately 70,000 (cycles are square wave, four per hour). Fixed deflection specimens of the wedge opening loading and stress-bend type are also included in the program. Detailed summary tabulations of these specimens are given in Quarterly Progress Report No. 18, GEAP-10120.

3.1 FATIGUE MACHINE AND SPECIMEN

The fatigue test unit (Figures 3-1 and 3-2) is placed in a stainless steel pressure vessel with a reciprocating shaft to bend 150 samples simultaneously. These are arranged radially at different levels and secured to the actuator ram, which is driven by an oil hydraulic system. The bending and displacement of the cantilever samples is determined by the ram travel and the clearances in outboard contacts. Specially devised instrumentation will detect and record the number of cycles to failure for each specimen. A drawing of the specimen is shown in Figure 3-3. A three-stage, axial hole located on the neutral axis of the specimen and centered in the reduced thickness transverse section is used to detect specimen failure. This axial hole is connected to an external pressure switch by capillary tubing. Failure is defined as a partial thickness fatigue crack sufficiently deep (~ 0.036 inch) to expose this axial cavity, previously at ambient pressure, to full test vessel pressure (1000 psi). This resultant rise in pressure is sensed by the external pressure switch and signals specimen failure.

Extensive shakedown and checkout tests have been performed on the test equipment with strain gages on the samples to ensure that the strain ranges specified are met consistently. Besides the evaluation of the specimen in air, simulated environmental testing at BWR conditions was carried out to 90,000 stress cycles. Proper functioning of the fatigue machine and of the failure detection and cycle counter system was demonstrated. The Type-304 specimens failed in the number of cycles predictable from "air" data. Nine fatigue specimens corresponding to the final design were tested to failure at room temperature in air; six more were tested to failure while immersed in $\sim 500^{\circ}\text{F}$, 1000 psig water. The results shown in Figure 3-4 indicate that:

- a. The presence of the axial holes does not significantly affect fatigue crack initiation.
- b. The major portion of total specimen fatigue life is consumed in fatigue crack initiation; only a relatively small portion is devoted to fatigue crack propagation.

3.2 DRESDEN I TEST LOOP

A materials testing loop (Figure 3-5) has been installed at the Dresden I Nuclear Power Station of the Commonwealth Edison Company. The test facility is in the "B" secondary heat exchanger loop. A nominal 10 gpm of Dresden I primary coolant flows to each of two test vessels. Valving has been included to accommodate a third test vessel currently being designed to contain a fatigue unit for crack growth rate measurement on WOL specimens. Instrumentation is provided to measure and record the pressure, flow rate, and inlet and outlet temperature of the water circulating through both test vessels. A safety system automatically isolates the loop and suspends dynamic loading of the fatigue specimens whenever any out-of-normal condition occurs.

3.2.1 WATER CHEMISTRY

Chemical parameters will be measured during operation of the test loop: specific conductance, oxygen, pH, chloride, fluoride and crud level.

Figure 3-1. Fatigue Test Machine for Materials Test Loop at Dresden I Nuclear Power Station

Figure 3-2. Completed Low-Cycle Fatigue Unit Prior to Placement in its Autoclave. All Specimens are in place and the Capillary Tubes which signal Failure are led through Seals in the Head to Pressure Switches on Top.

Figure 3-4. Type-304 Stainless Steel, Low-Cycle Fatigue versus Cycles to Crack

Figure 3-5. Schematic Diagram of Materials Testing Loop at Dresden I

The sampling and recording equipment was produced and assembled in three relay racks. Rack 1 contains the measuring instrumentation for the determination of pH, chloride content, and crud level on an intermittent basis. Functions are isolated from the other equipment packages so that this station may be shut down between visits to the plant.

pH measurement: pH will be measured by passing the primary coolant through a 3 X 4 X 1½ inch clear Lucite block containing a glass and reference electrode. The cell is designed so that there is a minimum of space between the tips of the electrodes to minimize high-impedance values in solution. Flow from the cell is directed to the rotameter, and then to drain. The glass and reference electrodes will be connected to an Orion pH meter with a digital readout. Divisions for recording the pH at a remote location are provided on the back of the instrument (0 to 10mV).

Crud level: The primary coolant will be filtered through an HA grade (0.45 micron) Millipore filter mounted in a stainless steel high-pressure filter holder. The effluent from the filter will be passed through a rotameter and then to drain. The Millipore samples containing the collected crud will be analyzed for iron, nickel, and copper.

Chloride levels: The chloride concentration in the primary coolant will be determined by analyzing the Cl-38 in the reactor water. This shortlived (half-life = 37 minutes) radionuclide will be isolated by wet chemical separations and analyzed by gamma spectrometry.

Specific conductance: Specific conductance of the reactor water will be determined by passing the sample through a conductivity cell (0.01 cell constant) to a temperature probe, and then an alarm rotameter. The output signals of the conductivity cell and temperature probe will be fed to an L&N high-purity water monitor, which provides a meter readout of specific conductance (0 to 1 µmho).

Oxygen content measurements: Two methods of monitoring oxygen content are planned:

Polarograph determination of oxygen using Beckman Model 735 process oxygen monitoring: The sample will be passed through a detection cell, alarm rotameter circuit, and then to drain.

Oxygen measurements using the thallium column method: Oxygen is measured in this method by the differential conductivity across the column filled with thallium metal. The sample will be passed through a conductivity cell, through a 1-inch-i.d. by 18-inch-long stainless steel column filled with thallium metal, to a second conductivity cell, to an alarm rotameter, and then to drain. The output of the first conductivity cell represents the conductivity of the influent stream. The output of the second conductivity cell represents the specific conductance of the influent stream, plus that amount of oxygen which reacted with the thallium metal to release thallium hydroxide. The differential conductivity is directly proportional to the oxygen content.

3.3 AIR FATIGUE TEST FACILITY

An "air" test facility has been assembled at San Jose to acquire base-line air fatigue data. Identical fatigue specimens, loaded in the same manner as those cycled in the Dresden test loop, will be tested in 500°F, 1000-psig air. High-pressure air is used so that failure can be detected, and therefore defined in essentially the same manner as for the Dresden loop specimens.

3.4 REACTOR PRESSURE VESSEL MATERIALS

An industry cooperative program has been set up to expose, in the Dresden materials test loop, materials as listed in Table 3-1. These represent currently used and "next generation" reactor pressure vessel steels. WOL and stress-bend specimens are planned, which will be placed in the same vessel with the piping fixed deflection specimens and examined periodically on the same schedule as the pipe materials. Present participants are: AEC, Commonwealth Edison, General Electric, Lukens Steel, Oak Ridge National Laboratory (HSST Program), Naval Research Laboratory, and Chicago Bridge & Iron.

Table 3-1
REACTOR PRESSURE VESSEL STEELS—
WOL AND STRESS-BEND SPECIMENS

	Base Metal	Weld Metal		Heat Affected Zone	
		Electroslag	Submerged Arc	Electroslag	Sub-Arc
A508	X				
A533B Class I	X	X	X	X	X
A542	X		X		X
A543	X		X		X

NOTE: Stress bends are to be made of base metal only.
An X indicates specimens planned.

3.5 ELECTROCHEMICAL CHARACTERIZATION OF COOLANT (R. L. Cowan)

The objective is to characterize quantitatively the corrosive nature of the coolant environment and its electrochemical interaction with the piping materials in the Task K loop. This will be accomplished by the automatic, periodic determination of specimen corrosion potential (E_{corr}) and corrosion current (i_{corr}) by linear polarization measurements.

Two electrochemical flanges have been installed: one immediately downstream of the vessel containing 150 fatigue specimens; and the other at the inlet to the materials test loop. Each flange contains electrically insulated penetrations on which the electrodes are mounted (see Figure 3-6). The electrodes are cylindrical specimens of 1/4-inch diameter and 1/2-inch length. Each flange contains two Type-304 stainless steel electrodes and two ASTM A-106B carbon steel electrodes that are being monitored for corrosion rate and corrosion potential on an automatic basis. In addition, the inlet flange has two additional electrodes of each type reserved for the determination of various electrochemical kinetic parameters. Each flange also has a platinum reference electrode and an auxiliary electrode disc made of Type-304 stainless steel. Prior to mounting, each electrode was carefully weighed to the nearest tenth milligram so that descaled weight loss measurements can be made at the completion of the exposure.

The flange sketch (Figure 3-6) shows that specimens are electrically insulated from the flange, and the only metallic part of the electrode assembly that "sees" the environment is the sample. The electrical contact to the flange is made by ten pair-twisted shielded cable.

A Petrolite ten-station corrosion rate meter (Model No. M-1010-C1) is used to record E_{corr} with respect to the platinum electrode and the linear polarization current, i_L , on a semicontinuous basis. The corrosion potential is measured through a high impedance circuit so that the sample is not polarized, and this value is recorded on a strip chart and also stored in the memory circuit of the meter. By a series of feedback amplifiers, the potential of the sample is then adjusted to a value of $E_{\text{corr}} - 10$ mV with respect to the platinum electrode by driving a current to or from the electrode by means of the auxiliary electrode. This current, i_L , is then recorded after 10 minutes. After a switching operation, the meter performs the same operations on the next specimen, etc. The unit operates for 2 hours out of every 24.

The value of i_L can be used to calculate the corrosion rate by the use of linear polarization theory. For small changes from steady state, the value $\frac{\Delta \eta}{\Delta i}$ is inversely related to corrosion rate by the relation:

$$\frac{\Delta \eta}{\Delta i}$$

Figure 3-6. Electrochemical Flange Assembly

$$i_{\text{corr}} = \frac{\beta_c \times \beta_a}{2.3 (\beta_a + \beta_c) \frac{\Delta\eta}{\Delta i}}$$

For the purposes of this study, the value $\Delta\eta$ is equal to $10 \text{ mV} - E_{\Omega}$, where E_{Ω} is the value of the resistive component of the overpotential, and can be measured separately for each electrode. The term Δi is merely i_L . Linear polarization theory is only applicable when both the half-cell reduction reaction and oxidation reaction are activation controlled, and we are assuming that this is the case for piping materials in reactor water. The parameters β_a and β_c are determined on the extra electrodes in Flange No. 1 by anodic and cathodic polarization curves.

4. FRACTURE MECHANICS

(M. B. Reynolds)

This task was devoted to a study of the relations between pressure load limit and flaw size in carbon steel pipes and fittings loaded monotonically to failure. In straight pipe segments, both through-wall and part-through flaws in both the axial and circumferential direction have been studied. Only part-through flaws were studied in fittings.

Of the flaw configurations investigated, two permitted mathematical analysis of results: the axial through-wall flaw which could be characterized by a single parameter (its length), and the full-length, part-through flaw which could be characterized by the thickness of the ligament remaining at the bottom of the flaw. It was found that for a pipe containing an axial through-wall flaw of half-length C , the limiting hoop stress, σ_h could be predicted by an empirical equation of the form:

$$\sigma_h^2 = \frac{K_c^2 \cos \theta}{\left(\pi C \left(1 + 0.4 \frac{C^2}{Rt} \right) \right)},$$

in which R is the pipe radius, t is its wall thickness, $\cos \theta$ is a plastic zone correction dependent on σ_h and σ_0 the burst stress for an unflawed pipe, and K_c is a material property parameter having the dimensions of a stress intensity factor ($\text{ksi} \sqrt{\text{in.}}$).

The pressure load limit of a pipe containing a full-length, part-through flaw could be estimated by analogy to a large and a small tensile element in series; i.e. by assuming that the load limit in the flawed pipe coincided with occurrence of plastic instability in the ligament remaining at the base of the flaw. In this analysis, the flaw was described in terms of X/T , the ratio of the thickness of the ligament remaining at the base of the flaw to the unflawed wall thickness T . The average radial stress in the ligament was assumed to be approximately equal to the product of the elastic Poisson ratio and the sum of the axial and circumferential stresses. Similar to the yield point observed in the stress-strain curve for low-carbon steels, the plastic instability stress in the ligament was assumed to be the yield strength of the steel.

For the more realistic case of short, part-through flaws, a failure diagram was proposed (see Figure 4-1). Pressure load limit, expressed as a fraction of that for an unflawed pipe of identical diameter and wall thickness, is plotted against the reciprocal of flaw length.¹² On such a plot, a line parallel to the abscissa at the value $P/P_0 = 1$ represents all flaws for which $X/T = 1$ (i.e., zero flaw depth). The lower curved line represents all flaws for which $X/T = 0$ (through-wall). The dashed curves for flaws having other values of X/T lie between the upper straight line and the lower curve at heights dependent upon X/T .

For elbows and tees containing part-through flaws, it was found that load limit, for a given flaw size, was most reduced by those flaws perpendicular to the maximum principal stress and in locations in which the maximum to intermediate principal stress ratio was highest.¹³

5. COMPUTER CODE DEVELOPMENT

The objective of the Stress Analysis task was to develop a computer code for the analysis of complex piping components. Subsequent to the initial large problem analysis for which promising results were reported, attempts to apply the PAPA code to additional cases encountered limited success. A special engineering assessment of the PAPA—Plate and Panel Analysis—code was conducted to determine: the limits of applicability, and the status in relation to highly developed codes in other areas such as the HSST program or NASA. The assessment led to the recommendation that further development work on PAPA be suspended in view of the advanced status of other codes. The assessment indicated that the computer program developed by Clough and his associates at the University of California appeared realistic and closest to successful completion. Other codes evaluated were: SAMIS, FORMAT, NASTRAN, ELAS, MAGIC, and SAFE-3D. The factors considered included element types, input-output capability, problem size capability, and program language.

6. PROBABILITY STUDY

(S. A. Wilson)

The objective of this task is to estimate the reliability, with respect to leakage and severance, of the service lifetime of the primary piping system of a water-moderated power reactor. The "Distribution of Time to Damage" method of reliability estimation is described and illustrated by an example: The estimation of the probability of leakage at a selected pipe location due to low-cycle fatigue.

6.1 THE DISTRIBUTION OF TIME TO DAMAGE METHOD

A method of synthesizing probabilities of failure is necessary because of insufficient historical failure rate information for nuclear piping. The method devised relies on design uncertainties and the variability of material properties. It is illustrated in Figure 6-1 and is described as follows.

- a. Draw a probability diagram such as Figure 6-2. The diagram shows a block for each stage of damage: crack initiation, leakage, and severance. Lines connect the blocks to show how each stage of damage leads to the next. Three ideas relate to each line: (1) a damage mechanism, such as low-cycle fatigue; (2) a time to damage prediction equation for each applicable mechanism, such as for the number of cycles to grow a crack from initial depth through to leakage by low cycle fatigue; and (3) the probability that this event occurs in a chosen time for reactor service life, such as 40 years.
- b. From the appropriate technology, devise a damage prediction equation for each line.
- c. Choose a piping location, called a study surface, and assign a nominal value and an uncertainty distribution to each variable in the equation as applicable to that surface. Design values, results of experiments, and engineering judgment are all utilized. Thus, the method uses the best available information about how well the actual value of a variable is believed to agree with its nominal value.
- d. Select only those variables that make the major contributions to the variability of the equation. This is done by a propagation of errors estimate of the variance of the equation.
- e. Estimate the fraction of the distribution which lies less than 40 years, as illustrated in Figure 6-3. This fraction is the desired probability and is estimated by Monte Carlo, using importance sampling.

The distribution of time to damage method is similar to methods known as "stress versus strength,"¹⁴ and "requirement versus capability,"¹⁵ in that areas under probability distributions are used to estimate reliabilities.

The crack growth stage of low-cycle fatigue was chosen for first application of this method because of its importance and the technical feasibility of proposing a cycles-to-leak equation.

Figure 6-1. : -Distribution of Time to Damage Method

Figure 6-2. Piping Probability Diagram for One Pipe Study Surface and Damage Mechanism

Figure 6-3. Probability of Leakage

6.2 APPLICATION TO A PIPING STUDY SURFACE

For the low-cycle fatigue damage mechanism, a damage prediction equation for the number of cycles to grow a crack from some initiated depth through the wall to leakage was devised from the fracture mechanics relationship:

$$da/dn = A (\Delta K)^B, \text{ inches/cycle}$$

$$n_t = \frac{1}{A} \int_{a_0}^t \left(\frac{1}{\Delta K} \right)^B da = \frac{\bar{\Delta K}}{C} \int_{a_0}^t \left(\frac{1}{\Delta K} \right)^B da$$

where

da/dn = crack growth rate,

ΔK = stress intensity factor range,

(log) A and (log) C = intercept,

B = slope parameter,

n_t = cycles to leak,

a_0 = initial crack depth,

t = wall thickness,

$\bar{\Delta K}$ = antilog (mean log ΔK). Mean log ΔK depends on a_0 , t , B stress (in ΔK), and the ΔK function, but not on A or C. Mean log ΔK is the mean of the ΔK 's related to all the cycles required to grow the crack from a_0 to t , and

a = instantaneous crack depth.

The piping study surface having the highest design stress in the reference piping system was chosen. The design stress was assumed to be axial due mainly to system bending from thermal expansion, and for this reason was considered to apply to only 1/4 of the circumference. Thus, the study surface was taken as 1/4 circumference by 1 inch in length, or 5.2 square inches for the 6-inch pipe. The 1-inch length was chosen for this reference case. The study surface may be any size, but it must be subject to one design value of each variable. With the governing stress field axial, a distribution of initial circumferential defects was used. The nominal value of each variable, and the source of the value, are given in Table 6-1. The distribution of each variable is illustrated in Figure 6-4, and the source of each distribution is also given in Table 6-1. Further details on selection of nominal values and distributions are included in the following paragraph.

The mean and variance of material parameters log A and B were estimated from testing conducted under Tasks D-1 and D-2 at the Materials and Processes Laboratory, Schenectady. The normal distribution was assumed for each. The log A value was chosen for the material, temperature level, and loading pattern of the study surface; the B value was common for all. The material is Type-304 stainless steel, and test results at room temperature were used as most characteristic of the operating conditions for the loop involved. The zero-to-tension loading pattern was chosen as most characteristic of piping loading between non-operating and operating conditions. For stress, an uncertainty distribution was sketched for each cause of uncertainty that was believed important, such as stress analysis model error, installation arrangement error, restricted flexibility error, etc. Also, for each cause a conditioning probability was assigned to reflect the probability with which each cause was judged to occur in a piping loop or component. The distribution illustrated in Figure 6-4 is the product of the individual-cause distributions. For initial crack depth, the distribution in Figure 6-4 is for the maximum initial crack in the study surface, since the deepest crack in a region of otherwise uniform properties will be the first to leak. This distribution was derived from an assigned uncertainty distribution for initial crack depth ratio, a_0/t , as shown in Figure 6-5 (without ultrasonic testing case), and the assumption that crack frequency is Poisson distributed with an assigned mean crack occurrence rate of $\lambda = 0.1$ per square inch.

6.3 PRELIMINARY CONCLUSIONS

The solution for the variance of n_t , using the propagation of errors method¹⁶ having the general formulation shown below, indicated that the contributions of the variability of wall thickness, diameter, and operating pressure make negligible contributions to the variability of n_t . It was also found that the variability of B makes little contribution to the variability of n_t . The uncertainty distributions of these variables, therefore, were not included in the following estimate. It was also found that the distribution of initial crack depth made the largest contribution to the distribution of n_t , thus identifying the variable most requiring better definition in this reference case:

$$Vn_t \cong \left(\frac{\partial n_t}{\partial A} \right)^2 VA + \left(\frac{\partial n_t}{\partial B} \right)^2 VB + \text{term for each variable,}$$

where

$V \equiv$ variance.

Because the distribution of n_t is analytically inaccessible, and small probabilities less than 10^{-2} must be estimated, assuming a distribution shape for n_t appeared unwise. It was decided to estimate the required probability by Monte Carlo with importance sampling, a variance-reduction method.^{17 18} Importance sampling directs the uniformly distributed random numbers primarily to regions of the variables so that the function more often gives results in the range for which the probability is desired. Then, rather than 1 being assigned to each such favorable outcome, a weight is assigned which is the

* PROBABILITY DENSITY FUNCTIONS

Figure 6-4. Uncertainty Distributions for Log C, B, $a_o/t \text{ Max}$, and Stress

Figure 6-5. Judgmental Uncertainty Distribution for a_0/t , Initial Crack Depth Ratio

reciprocal of the density of the weighting function associated with the random number chosen. This process is illustrated in Figure 6-6, where the arrow initiates a typical random solution. Monte Carlo is not required for this one-dimensional illustration, of course, whereas it may be when "X" is a function of other variables. Fortunately, n_t is monotonic in $\log A$ (or $\log C$), stress and a_0/t , so the general region to sample from each of these variables to give low n_t values is known. The weighting function used for each variable is described elsewhere.¹³

A 3000-trial estimate of the probability that n_t is less than 360 cycles—operating cycles in 40 years—was made for the reference conditions of Table 6-1, without ultrasonic testing. The estimate was 0.0077. 2σ confidence limits on the estimate were ± 0.0017 , reflecting a reduction to 0.18 of the estimate variance which would have been obtained by crude Monte Carlo. Thus, for the reference conditions, the probability of leakage in a service lifetime at the study surface due to low-cycle fatigue was found to be approximately 0.008. This estimate will be larger for the piping loop, but other surface increments are subject to lower design stresses and therefore, lower probability values.

6.4 EXTENSIONS FOR SYSTEM RELIABILITY ESTIMATE

In principle, similar estimates are required at all other study surfaces of the piping system. Doing this is not feasible, but it is possible to restrict attention to those locations having sufficiently high design stress to govern the probability of leakage anywhere in the system. A simple graphical relationship among probability of leakage to nominal stress, and to wall thickness, is anticipated, permitting probability values to be obtained readily for the design values of any study surfaces.

The probability of leakage anywhere in the system, in 40 years, is found by appropriately summing over the piping system the probabilities at each of the study surfaces. This must be done in order to reflect lack of independence among actual stress levels at successive study surfaces; but all variables at one study surface, and the other variables at successive study surfaces, appear to be independent.

Extensions are also required for both initiation and growth for other mechanisms, such as stress corrosion and fatigue-assisted stress corrosion, to more extensive critical size definitions, and to knowledge of conditions that favor reaching critical size for severance without prior detectable leakage.

Table 6-1
NOMINAL VALUES USED, AND
SOURCES OF NOMINAL VALUES AND DISTRIBUTIONS

Variable	Nominal Value Used	Source		
		Design Value	From Test Data	From Assigned Uncertainty Distribution
Initial crack depth ratio, a_o/t	(0.036)			Nom. and Distr. Value Assigned
Crack occurrence rate, λ	0.1			
Slope parameter, B	4.45		Nom. Distr.	
Intercept parameter, Log A	For SS 304 at R. T. 5.5×10^{-26}		Nom Distr.	
Expansion stress, σ (in ΔK), psi	27,450	Nom.		Distr.
Wall Thickness, t, in.	0.432	Nom.		Distr.
Diameter, d, in.	6.624	Nom.		Distr.
Pressure, P, psig	1,000	Nom.		Distr.

Nom.—Shows source of nominal value used.

Distr.—Shows source of uncertainty distribution used.

ASSIGN WEIGHT $1/4.96$ TO THIS FAVORABLE OUTCOME TRIAL; ASSIGN WEIGHT 0 TO AN UNFAVORABLE OUTCOME ($X > x_0$). $\Pr(X \leq x_0)$ IS ESTIMATED AS THE MEAN OF WEIGHTS TAKEN OVER ALL TRIALS.

Figure 6-6. Monte Carlo Using Importance Sampling to Estimate $\Pr[X \leq x_0]$

REFERENCES

1. Kilsby, E. R., Jr., "Reactor Primary Piping System Rupture Studies," *Nuclear Safety*, **7** (2): 185-194 (Winter 1965-1966).
2. Vandenberg, S. R., "Status of Pipe Rupture Study at General Electric," *Nuclear Safety*, **8** (2): 148-155 (Winter 1966-1967).
3. Vandenberg, S. R., "Status of Pipe Rupture Study at General Electric—Part II," *Nuclear Safety*, **10** (3): 217-227 (May—June 1969).
4. General Electric Company, Atomic Power Equipment Department, *Reactor Primary Coolant System Rupture Study, Quarterly Progress Reports Nos. 1 through 20*, for July 1965 through March 1970, USAEC Reports Nos.: GEAP-4911, -4964, -5082, -5147, -5192, -5279, -5427, -5474, -5512, -5554, -5587, -5637, -5680, -5716, -5770, -10024, -10072, -10120, -10143, and -11069.
5. Krempl, E., *The Effect of Stress Concentrations on Low-Cycle Fatigue at Room Temperature and 550°F for Three Low-Strength Structural Steels*, March 1970, (GEAP-10170).
6. Brothers, A. J., *Fatigue Crack Growth in Nuclear Reactor Piping Steels*, March 1968, (GEAP-5607).
7. D. C., Gomez, M. P., and Anderson, W. E., "A Rational Analytical Theory of Fatigue," *The Trend in Engineering*, **13**, 1961, p.9.
8. Brothers, A. J., *Low-Cycle Strain-Controlled Fatigue Crack Propagation in Nuclear Piping Steels*, May 1970, (GEAP-10181).
9. Paris, P. C. and Sih, G. C., "Stress Analysis of Cracks," *Fracture Toughness Testing*, 1965, (ASTM STP 381).
10. Kiss, E., Heald, J. D., and Hale, D. A., *Low-Cycle Fatigue of Prototype Piping*, January 1970, (GEAP-10135).
11. Brothers, A. J. and Woodford, D. A., *The Effect of Cyclic Strain Aging on the Embrittlement of a Plain Carbon Steel Plate*, January 1970, (GEAP-10140).
12. Reynolds, M. B., *A Failure Diagram for Axially Flawed Pipes*, April 1968, (GEAP-5622).
13. *Quarterly Progress Report No. 20*, (GEAP-11069).
14. Disney, R. L. and Sheth, N. J., "The Determination of the Probability of Failure by Stress-Strength Interference Theory," *Annals of Assurance Sciences; Proceedings of the Annual Symposium on Reliability, Boston, Mass., January 16-18, 1968*, pp. 417-422.
15. Ingram, G. E., Hermann, C. R., and Welker, E. L., "Designing for Reliability Based on Probability Modeling Using Remote Access Computer Systems," *Proceedings, Seventh Reliability and Maintainability Conference, ASME et al., San Francisco, July 14-17, 1968*.
16. Hahn, G. J. and Shapiro, S. S., *Statistical Models in Engineering*, Wiley, 1967, p. 229ff.
17. Clark, Charles E., "Importance Sampling in Monte Carlo Analyses," *Operations Research*, Sept.—Oct. 1961, p. 603ff.
18. Hammersley, J. M. and Handscomb, D. C., *Monte Carlo Methods*, Wiley, 1964, p. 57ff.

DISTRIBUTION

Aerojet General Engineering Division Sacramento Plant Sacramento, California 95801 Attn. Dr. F.J. Climent	1	Advisory Committee on Reactor Safeguards Dr. Harry O. Monson, Senior Engineer Laboratory Director's Office Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois 60439	1
Advisory Committee on Reactor Safeguards Dr. Spencer H. Bush Consultant to the Director Battelle Memorial Institute Pacific Northwest Laboratory Richland, Washington 99352	1	Advisory Committee on Reactor Safeguards Dr. David Okrent, Senior Physicist Laboratory Director's Office Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois 60439	1
Advisory Committee on Reactor Safeguards Mr. Harold Etherington 84 Lighthouse Drive Jupiter, Florida 33458	1	Advisory Committee on Reactor Safeguards Dean Nunzio J. Palladino College of Engineering The Pennsylvania State University 101 Hammond Building University Park, Pennsylvania 16802	1
Advisory Committee on Reactor Safeguards Dr. William L. Faith 2540 Huntington Drive San Marino, California 91108	1	Advisory Committee on Reactor Safeguards Dr. William R. Stratton Los Alamos Scientific Laboratory P. O. Box 1663 Los Alamos, New Mexico 87544	1
Advisory Committee on Reactor Safeguards Dr. Chester P. Siess Department of Civil Engineering 3129 Civil Engineering Bldg. University of Illinois Urbana, Illinois 61801	1	Advisory Committee on Reactor Safeguards Dr. Carroll W. Zabel Director of Research University of Houston Cullen Boulevard Houston, Texas 77004	1
Advisory Committee on Reactor Safeguards Dr. Stephen H. Hanauer Professor of Nuclear Engineering 606 Dougherty Hall University of Tennessee Knoxville, Tennessee 37916	1	Mr. Raymond F. Fraley Executive Secretary	3
Advisory Committee on Reactor Safeguards Dr. Joseph M. Hendrie Nuclear Engineering Department Brookhaven National Laboratory Upton, New York 11973	1	Advisory Committee on Reactor Safeguards U.S. Atomic Energy Commission Room 1034-H Washington, D.C. 20545	
Advisory Committee on Reactor Safeguards Dr. Herbert S. Isbin Department of Chemical Engineering University of Minnesota Minneapolis, Minnesota 55455	1	Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois Attn: Mr. Paul G. Shewmon	1
Advisory Committee on Reactor Safeguards Mr. Harold G. Mangelsdorf 78 Knollwood Road Short Hills, New Jersey 07078	1	Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois Attn: Dr. P. Lottes	1
Advisory Committee on Reactor Safeguards Dr. Arlie A. O'Kelly 2421 West Rowland Avenue Littleton, Colorado 80120	1	Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois Attn: Dr. C.E. Dickerman	1

Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois Attn: Dr. R.O. Ivins	1	Atomic Energy Commission Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Mr. E.E. Kintner Asst. Director for Reactor Engineering	1
Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois Attn: Dr. S. Fistedis	1	Atomic Energy Commission Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Mr. J. W. Crawford Asst. Director for Engineering Standards	1
Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois Attn: Dr. R.C. Vogel	1	Atomic Energy Commission Division of Compliance, Region IV 10395 West Colfax Avenue Denver, Colorado 80215 Attn: Mr. John W. Flora	1
Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois Attn: LMFBR Program Office	1	Atomic Energy Commission Division of Compliance Washington, D.C. 20545 Attn: Mr. L. Kornblith, Jr.	1
Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois Attn: Mr. A. Amorosi	2	Atomic Energy Commission Division of Operational Safety Washington, D.C. 20545 Attn: Mr. H. Gilbert	1
Argonne National Laboratory 9700 South Cass Avenue Argonne, Illinois Attn: Dr. L. Baker	1	Atomic Energy Commission Division of Reactor Standards Washington, D.C. 20545 Attn: Mr. E.G. Case	10
Atomic Energy Commission Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Col. R.L. Ednie Asst. Director for Army Reactors	1	Atomic Energy Commission Division of Reactor Standards Washington, D.C. 20545 Attn: Mr. M. Bolotsky	1
Atomic Energy Commission Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Mr. M.A. Rosen Asst. Director for Plant Engineering	1	Atomic Energy Commission Division of Reactor Standards Washington, D.C. 20545 Attn: Mr. A.B. Holt	1
Atomic Energy Commission Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Mr. M.J. Whitman Asst. Director for Program Analysis	1	Atomic Energy Commission Division of Reactor Standards Washington, D.C. 20545 Attn: Mr. R. Waterfield	1
Atomic Energy Commission Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Dr. E.E. Sinclair Asst. Director for Reactor Technology	1	Atomic Energy Commission Division of Reactor Standards Washington, D.C. 20545 Attn: Dr. G. Burley	1
Atomic Energy Commission Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Mr. A. Giambusso Asst. Director for Project Management	1	Atomic Energy Commission Division of Reactor Standards Washington, D.C. 20545 Attn: Mr. R. Impara	1

Atomic Energy Commission Water Projects Branch Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Mr. W.H. Layman	1	Battelle Memorial Institute 505 King Avenue Columbus, Ohio 43201 Attn: Dr. D.L. Morrison	1
Atomic Energy Commission Naval Reactors Branch Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Mr. R.S. Brodsky	2	Battelle Memorial Institute 505 King Avenue Columbus, Ohio 43201 Attn: Mr. S. Paprocki	2
Atomic Energy Commission Division of Reactor Development and Technology Washington, D.C. 20545 Attn: A.J. Pressesky	7	Battelle Memorial Institute 505 King Avenue Columbus, Ohio 43201 Attn: Mr. A.R. Duffy	1
Atomic Energy Commission Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Mr. S.A. Szawlewicz	5	Brookhaven National Laboratory Upton, Long Island, New York 11973 Attn: A.W. Castleman	1
Atomic Energy Commission Division of Reactor Development and Technology Washington, D.C. 20545 Attn: Mr. R.R. Newton	1	University of California Institute of Engineering Research Berkeley, California 94704 Attn: Prof. V.E. Schrock	1
Atomic Energy Commission Division of Production Washington, D.C. 20545 Attn: Mr. George B. Pleat	1	University of California Institute of Engineering Research Berkeley, California 94704 Attn: Prof. H.A. Johnson	1
Atomic Energy Commission c/o Gulf General Atomic, Inc. P.O. Box 608 San Diego, California 92112 Attn: Mr. Russell H. Ball	1	Canoga Park Area Office P.O. Box 591 Canoga Park, California 91305 Attn: Mr. R.L. Morgan RDT Senior Site Rep.	1
Atomics International P.O. Box 309 Canoga Park, California Attn: Dr. H. Morewitz	2	Combustion Engineering, Inc. Nuclear Division P.O. Box 500 Windsor, Connecticut 06095 Attn: Mr. M.F. Valerino	1
Babcock & Wilcox Company Washington Operations Office 1725 I Street, N.W. Washington, D.C. 20006	1	Chicago Operations Office Atomic Energy Commission 9800 South Cass Avenue Argonne, Illinois 60439 Attn: Mr. D.M. Gardiner	1
Babcock & Wilcox Company P.O. Box 1260 Lynchburg, Virginia Attn: Mr. Robert Wascher	1	Douglas United Nuclear Richland, Washington Attn: Mr. John Riches	1
Battelle Memorial Institute 505 King Avenue Columbus, Ohio 43201 Attn: Dr. D.N. Sunderman	2	Harvard Air Cleaning Laboratory Harvard University 665 Huntington Avenue Boston, Massachusetts 02190	1
		IIT Research Institute 10 W. 35th Street Chicago, Illinois 60616 Attn: Dr. T.A. Zaker	1

GEAP-1025

IIT Research Institute 10 W. 35th Street Chicago, Illinois 60616 Attn: Mr. E.V. Gallagher	1	Chemical Technology Division Oak Ridge National Laboratory P.O. Box Y Oak Ridge, Tennessee 37830 Attn: Mr. R. Blanco	1
Idaho Operations Office Atomic Energy Commission P.O. Box 2108 Idaho Falls, Idaho 83401 Attn: Mr. D. Williams	2	Oak Ridge National Laboratory P.O. Box Y Oak Ridge, Tennessee Attn: HTGR Safety Program Office	2
Liquid Metal Engineering Center c/o Atomics International P.O. Box 309 Canoga Park, California 91304 Attn: R.W. Dickinson	1	Oak Ridge National Laboratory P.O. Box Y Oak Ridge, Tennessee Attn: Mr. W. B. Cottrell	4
Los Alamos Scientific Laboratory P.O. Box 1663 Los Alamos, New Mexico 87544 Attn: Mr. J.H. Russel, K Division	1	Oak Ridge National Laboratory P.O. Box Y Oak Ridge, Tennessee Attn: Mr. P. Rittenhouse	1
MPR Associates, Inc. 1140 Connecticut Avenue, N.W. Washington, D.C. 20036 Attn: Mr. T. Rockwell III Chairman AIF Safety Task Force	1	Pacific Northwest Laboratories P.O. Box 999 Richland, Washington 99352 Attn: Mr. G. Rogers	1
National Bureau of Standards Washington, D.C. 20545 Attn: Dr. C. Muehlhaue	1	Pacific Northwest Laboratories P.O. Box 999 Richland, Washington 99352 Attn: Mr. J.C. Spanner	1
Naval Ordnance Laboratory White Oak Silver Spring, Maryland Attn: Mr. James Proctor	1	Pacific Northwest Laboratories P.O. Box 999 Richland, Washington 99352 Attn: Dr. J. Batch	1
North Carolina State University Department of Mechanical Engineering Raleigh, North Carolina 27607 Attn: Prof. M.N. Ozisik	1	Pacific Northwest Laboratories P.O. Box 999 Richland, Washington 99352 Attn: Mr. R. Nightingale	2
Nuclear Fuels Service West Valley, New York 14171	1	Pacific Northwest Laboratories P.O. Box 999 Richland, Washington 99352 Attn: Mr. L. Schwendiman	1
Oak Ridge Operations Office Atomic Energy Commission Oak Ridge, Tennessee 37830 Attn: Mr. W.L. Smalley	1	Pacific Northwest Laboratories P.O. Box 999 Richland, Washington 99352 Attn: Mr. E.R. Astley, Mgr. FFTF	1
Oak Ridge National Laboratory Nuclear Safety Information Center P.O. Box Y Oak Ridge, Tennessee 37830 Attn: Mr. Joel Buchanan	1	Idaho Nuclear Corporation P.O. Box 1845 Idaho Falls, Idaho 83401 Attn: Mr. Curt Haire	1
Chemical Technology Division Oak Ridge National Laboratory P.O. Box Y Oak Ridge, Tennessee 37830 Attn: Mr. D. Ferguson	1	Idaho Nuclear Corporation P. O. Box 1845 Idaho Falls, Idaho 83401 Attn: Water Reactor Safety Program Office, Mr. G. O. Bright, Mgr.	3

Idaho Nuclear Corporation P. O. Box 1845 Idaho Falls, Idaho 83401 Attn: Mr. S. O. Johnson	2	Westinghouse Electric Corporation Atomic Power Division P.O. Box 355 Pittsburgh, Pennsylvania 15230 Attn: Dr. D. Fletcher	1
Idaho Nuclear Corporation P.O. Box 1845 Idaho Falls, Idaho 83401 Attn: Mr. H. L. Coplen	2	Westinghouse Electric Corporation Atomic Power Division P.O. Box 355 Pittsburgh, Pennsylvania 15230 Attn: Dr. E. Beckjord	1
Idaho Nuclear Corporation P. O. Box 1845 Idaho Falls, Idaho 83401 Attn: Mr. O. F. Brockett	1	Westinghouse Electric Corporation P.O. Box 19218 Tampa, Florida 33616 Attn: Mr. A. Lohmeier	1
Richland Operations Office P.O. Box 500 Richland, Washington 99352 Attn: Mr. C. Robinson	1	Southern Nuclear Engineering, Inc. P.O. Box 10 Dunedin, Florida 33528 Attn: Mr. Gilbert Brown	1
Richland Operations Office P.O. Box 500 Richland, Washington 99352 Attn: Mr. A.S. Waterhouse	1	Holmes & Narver, Inc. 828 South Figueroa St. Los Angeles, California 90017 Attn: B. Shimizu	1
Richland Operations Office P.O. Box 500 Richland, Washington 99352 Attn: Mr. A. Brunstad	1	Mr. Edward T. Wessel Research and Development Center Westinghouse Electric Corporation Beulah Road, Churchill Boro Pittsburgh, Pennsylvania 15235	1
San Francisco Operations Office Atomic Energy Commission 2111 Bancroft Way Berkeley, California 94704 Attn: Mr. C.V. Backlund	1	Dr. William E. Cooper Teledyne Materials Research 303 Bear Hill Road Waltham, Massachusetts	1
Savannah River Laboratories E.I. duPont deNemours and Company Aiken, South Carolina 29802 Attn: Mr. A.H. Peters	1	Roger W. Staehle Metallurgy Department Ohio State University Columbus, Ohio	1
TRW Inc. TRW Systems Group One Space Park Redondo Beach, California 90278 Attn: Dr. D. B. Langmuir	1	Mr. Ralph Jones Division of Reactor Development U.S. Atomic Energy Commission Washington 25, D.C.	1
TRW Inc. TRW Systems Group One Space Park Redondo Beach, California 90278 Attn: Mr. S.M. Zivi	1	Mr. H.K. Marks Room 2N83 Department of the Navy Washington, D.C.	1
Westinghouse Electric Corporation Atomic Power Division P.O. Box 355 Pittsburgh, Pennsylvania 15230 Attn: Mr. H.A. Wiesemann	1	Mr. F.M. Moschine Westinghouse Electric Company Atomic Power Department P.O. Box 355 Pittsburgh, Pennsylvania 15230	1

H. Thielsch 140 Shaw Avenue Cranston 5, Rhode Island	1	United Kingdom Atomic Energy Authority Reactor Materials Laboratory Wigshaw Lane, Culcheth Warrington, Lancs. England Attn: R.W. Nichols	1
Dr. P. L. Pfenningwerth Bettis Atomic Power Laboratory P.O. Box 79 West Mifflin, Pennsylvania 15122	1	Atomic Energy of Canada Limited Chalk River Nuclear Laboratories Chalk River, Ontario, Canada Dr. George Pon	1
Professor C.E. Taylor Department of Theoretical and Applied Mechanics University of Illinois Urbana, Illinois	1	Mr. Robert D. Wylie Department of Materials Engineering Southwest Research Institute 8599 Culebra Road San Antonio, Texas 78228	1
Mr. E. Beauchamp-Nohhs U.S. Marine Engineering Laboratory Annapolis, Maryland	1		
U.S. Atomic Energy Commission Division of Technical Information Extension P.O. Box 62 Oak Ridge, Tennessee	3	O.A. Kellerman Institut Fur Reaktorsicherheit Der Technischen Uberwachungs Vereine, e.V. 5 Koln - Ehrenfeld, Lukasstr. 90 West Germany	1
U.S. Atomic Energy Commission Division of Reactor Licensing Washington, D.C. 20545 Attn: S.S. Pawlicki	1	C. A. G. Phillips U.K.A.E.A. Safeguards Division Authority Health & Safety Branch Risley, Warrington, Lancashire England	1
USAEC Site Representative General Electric Company Sunnyvale, California 94086 Attn: Joel Levy, Senior Site Rep.	1	Knolls Atomic Power Laboratory P. O. Box 1072 Schenectady, New York 12301 Attn: Dr. Robert A. Barnes	1
Mr. B.L. Greenstreet P.O. Box Y Oak Ridge National Laboratory Oak Ridge, Tennessee	1	Reactor Materials Branch Metallurgy Division Naval Research Laboratory Washington, D. C. 20545 Attn: Mr. L. E. Steele	1
Mr. F.J. Witt P.O. Box Y ORNL - Oak Ridge, Tennessee	1		
Commonwealth Edison Company Dresden Nuclear Power Station Rural Route 1 Morris, Illinois 60450 Attn: H.K. Hoyt	2	Department of Material Science & Engineering Hearst Mining Building University of California Berkeley, California Attn: Mr. William W. Gerberich	1
Commonwealth Edison Company System Mechanical and Structural Engineer 72 West Adams Street Chicago, Illinois 60690 Attn: N.A. Kershaw	3	United Engineers and Constructors, Inc. 1401 Arch Street Philadelphia, Pennsylvania 19105 Attn: Mr. John Crowley	1

Argonne National Laboratory
9700 South Cass Avenue
Argonne, Illinois
Attn: Mr. Craig Cheng

1 Division of Reactor Development & Technology 1
U.S. Atomic Energy Commission
Washington, D.C. 20545
Attn: J. R. Hunter

AEG Telefunken
AEC Hochhaus Sued
6 Frankfurt/Main 70
West Germany
Attn: Mr. Dieter Ewers

1 Asst. Director, Instruction Division 1
U.S. Atomic Energy Commission
Chicago Operations Office
9800 South Cass Avenue
Argonne, Illinois 60439
Attn: R. M. Moser