1 of 2

NUREG/CR-6233
BMI-2177
Vol. 1

Stability of Cracked Pipe Under Inertial Stresses

Subtask 1.1 Final Report

Manuscript Completed: July 1994
Date Published: August 1994

Prepared by
P. Scott, M. Wilson, R. Olson, C. Marschall, R. Schmidt, G. Wilkowski

Battelle
505 King Avenue
Columbus, OH 43201

Prepared for
Division of Engineering
Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
Washington, DC 20555-0001
NRC FIN D1658

MASTER

yp
DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

ABSTRACT

This report presents the results of the pipe fracture experiments, analyses, and material characterization efforts performed within Subtask 1.1 of the IPIRG Program. The objective of Subtask 1.1 was to experimentally verify the analysis methodologies for circumferentially cracked pipe subjected primarily to inertial stresses. Eight cracked-pipe experiments were conducted on 6-inch nominal diameter TP304 and A106B pipe. The experimental procedure was developed using nonlinear time-history finite element analyses which included the nonlinear behavior due to the crack. The model did an excellent job of predicting the displacements, forces, and times to maximum moment. The comparison of the experimental loads to the predicted loads by the Net-Section-Collapse (NSC), Dimensionless Plastic-Zone Parameter, J-estimation schemes, R6, and ASME Section XI in-service flaw assessment criteria tended to underpredict the measured bending moments except for the NSC analysis of the A106B pipe.

The effects of flaw geometry and loading history on toughness were evaluated by calculating the toughness from the pipe tests and comparing these results to $C(T)$ values. These effects were found to be variable. The surface-crack geometry tended to increase the toughness (relative to $C(T)$ results), whereas a negative load-ratio significantly decreased the TP304 stainless steel surface-cracked pipe apparent toughness. The inertial experiments tended to achieve complete failure within a few cycles after reaching maximum load in these relatively small diameter pipe experiments. Hence, a load-controlled fracture mechanics analysis may be more appropriate than a displacement-controlled analysis for these tests.

CONTENTS

	<u>Page</u>
ABSTRACT	iii
CONTENTS	v
EXECUTIVE SUMMARY	xix
ACKNOWLEDGMENTS	xxiii
NONMENCLATURE	xxv
1. INTRODUCTION	1-1
1.1 Background	1-1
1.2 Overview of IPIRG Program	1-2
1.3 Specific Objective of Inertially Loaded Pipe Fracture Subtask	1-2
1.4 References	1-4
2. MATERIAL CHARACTERIZATION EFFORTS	2-1
2.1 Material Selection	2-1
2.2 Tensile Test Results	2-2
2.2.1 Tensile Properties of Pipe DP2-F30, ASTM A106 Grade B Carbon Steel	2-2
2.2.2 Tensile Properties of Pipe DP2-A23, Type 304 Stainless Steel	2-3
2.3 Fracture Toughness Results	2-9
2.3.1 Fracture Toughness of Pipe DP2-F30, ASTM A106 Grade B Carbon Steel	2-10
2.3.2 Fracture Toughness of Pipe DP2-A23, Type 304 Stainless Steel	2-17
2.4 Discussion of Dynamic Rate Effects on Properties	2-19
2.5 References	2-21
3. PIPE FRACTURE EXPERIMENTS	3-1
3.1 Pretest Design Analysis	3-2
3.1.1 Pretest Design Approach	3-2
3.1.2 Pretest Design Analysis Methods	3-6

3.1.2.1	Structural Analysis	3-6
3.1.2.2	Structural Damping	3-6
3.1.2.3	Nonlinear Modeling of Cracked Pipe Sections	3-6
3.1.2.4	Modifications to the Nonlinear Model of Cracked Pipe Sections	3-7
3.1.2.5	Adjustments of Moment Due to Internal Pressure	3-8
3.1.2.6	Summary of Pretest Design Calculation Procedures Used	3-9
3.1.3	Finite Element Implementation of Moment Rotation Response	3-11
3.1.3.1	Servohydraulic Flow Analysis	3-15
3.1.4	Dynamic Loading Function	3-15
3.1.5	Results of Pretest Design Analyses	3-16
3.1.5.1	Inertially Loaded Carbon Steel Surface Crack Pretest Design Analysis	3-16
3.1.5.2	Inertially Loaded Stainless Steel Through-Wall Crack Pretest Design Analysis	3-19
3.1.5.3	Summary of Pretest Design Analyses	3-19
3.2	Description of Experimental Procedures	3-23
3.2.1	Test Facility	3-23
3.2.2	Pipe Specimen Preparation and Instrumentation	3-26
3.2.3	Data Acquisition	3-32
3.2.4	Data Reduction Procedures	3-33
3.3	Results of Experiments	3-36
3.3.1	Uncracked-Pipe Experiment	3-36
3.3.2	Through-Wall-Cracked-Pipe Experiments	3-38
3.3.2.1	Experiment 1.1-2	3-38
3.3.2.2	Experiment 1.1-3	3-39
3.3.3	Surface-Cracked-Pipe Experiments	3-39
3.3.3.1	Experiment 1.1-4	3-39
3.3.3.2	Experiment 1.1-5	3-43
3.3.3.3	Experiment 1.1-6	3-43
3.3.3.4	Experiment 1.1-7	3-47
3.3.3.5	Experiment 1.1-8	3-49
3.3.3.6	Experiment 1.1-9	3-49
3.4	References	3-52

4.	ANALYSIS OF CRACKED-PIPE FRACTURE EXPERIMENTS	4-1
4.1	Crack Initiation and Maximum Load Predictive Analyses	4-3
4.1.1	Net-Section-Collapse Analyses	4-3
4.1.2	Dimensionless Plastic-Zone Screening Criterion	4-4
4.1.3	J-Estimation Scheme Analyses	4-12
4.1.3.1	Material Property Input for J-Estimation Scheme Analyses	4-12
4.1.3.2	Analysis of Through-Wall-Cracked-Pipe Experiments	4-18
4.1.3.3	Results of Through-Wall-Cracked-Pipe J-Estimation Scheme Predictions	4-20
4.1.3.4	Results of Surface-Cracked-Pipe J-Estimation Scheme Analysis	4-22
4.1.4	R6 Revision 3 Option 1 Procedures	4-26
4.1.4.1	The Computational Procedure	4-26
4.1.4.2	Results of Comparisons of R6 Predictions with Experimental Results	4-28
4.1.5	ASME Section XI Flaw Evaluation Procedures	4-31
4.1.5.1	Austenitic Pipe Fracture Experiments	4-31
4.1.5.2	Ferritic Pipe Fracture Experiments	4-34
4.2	J-R Curves from Specimens and Pipe Experiments	4-36
4.2.1	J _i Values from Subtask 1.1 Pipe Experiments	4-38
4.2.1.1	Experiment 1.1-3; Inertial TWC TP304 Pipe Experiment	4-38
4.2.1.2	Experiment 1.1-5; Surface-Cracked TP304 Pipe Experiment	4-39
4.2.1.3	Experiment 1.1-7; Dynamic Monotonic Bending with Constant Internal Pressure of a Surface-Cracked A106 Grade B Pipe	4-40
4.2.1.4	Experiment 1.1-9; Quasi-static Monotonic Bending with Constant Internal Pressure of a Surface-Cracked A106 Grade B Pipe	4-40
4.2.2	J-R Curve from Subtask 1.1 Stainless Steel Through- Wall-Cracked-Pipe Experiment (1.1-3)	4-41
4.3	Dynamic Analyses	4-42
4.3.1	Dynamic Analysis of Experiment 1.1-3	4-42
4.3.2	Dynamic Analysis of Experiment 1.1-2	4-42
4.3.3	Elastic Stress Analysis	4-47
4.4	References	4-51

5.	DISCUSSION OF TECHNICAL RESULTS	5-1
5.1	Technical Results from Material Characterization Efforts	5-1
5.2	Technical Results from Pipe Fracture Experiments	5-2
5.3	Technical Results from Analytical Evaluations	5-5
5.3.1	Net-Section-Collapse Analysis of Experiments	5-5
5.3.2	Dimensionless Plastic-Zone Parameter Criterion	5-6
5.3.3	J-Estimation Scheme Predictions of Crack Initiation and Maximum Stresses	5-6
5.3.3.1	Through-Wall-Cracked-Pipe Analyses	5-6
5.3.3.2	Surface-Cracked-Pipe Analyses	5-6
5.3.4	R6 Revision 3 Option 1 Predictions	5-7
5.3.5	ASME Section XI Predictions	5-7
5.3.6	Calculation of J_i Values from Pipe Experiments	5-8
5.3.7	Dynamic Analyses of Pipe Fracture Experiments	5-9
5.4	References	5-11
6.	SIGNIFICANCE OF RESULTS ON LBB AND IN-SERVICE FLAW EVALUATION CRITERIA	6-1
6.1	Use of Linear-Elastic Stress Analysis for LBB and In-Service Flaw Evaluations	6-1
6.2	Cyclic Load History Considerations for Both LBB and In-Service Flaw Evaluations	6-1
6.3	Significance to In-Service Flaw Evaluation Criteria	6-2
6.3.1	ASME Section XI Stainless Steel Pipe Evaluations	6-2
6.3.2	ASME Section XI Carbon Steel Pipe Evaluations	6-4
6.3.3	R6 Revision 3 Option 1 Assessment	6-4
6.4	References	6-4
	APPENDIX A	A-1
	APPENDIX B	B-1

LIST OF FIGURES

	Page
Figure 1.1. Stability of center-cracked Type 304 stainless steel plates under cyclic displacement-control and cyclic load-control conditions	1-3
Figure 2.1 Effect of strain rate and test temperature on tensile properties of Pipe DP2-F30 (A106 Grade B carbon steel)	2-5
Figure 2.2 Engineering stress-strain curves for tensile specimens from Pipe DP2-F30 (A106 Grade B carbon steel), tested at at several different strain rates at 288 C (550 F)	2-6
Figure 2.3 True stress-strain curves for tensile specimens from Pipe DP2-F30 (A106 Grade B carbon steel), tested at several different strain rates at 288 C (550 F)	2-6
Figure 2.4 Engineering stress-strain curves for tensile specimens from Pipe DP2-A23 (Type 304 stainless steel), tested at several different strain rates at 288 C (550 F)	2-8
Figure 2.5 Strength versus strain rate at selected amounts of plastic strain for tensile specimens from Pipe DP2-A23 (Type 304 stainless steel), tested at 288 C (550 F)	2-8
Figure 2.6 J versus crack extension for Pipe DP2-F30 (A106 Grade B carbon steel) compact specimens tested quasi-statically at 288 C (550 F) at MEA	2-11
Figure 2.7 Transient recorder trace for dynamic C(T) test of A106 Grade B carbon steel specimen F30-109 at 288 C (550 F)	2-12
Figure 2.8 Load-displacement curve for dynamic C(T) test of A106 Grade B carbon steel specimen F30-109 at 288 C (550 F)	2-12
Figure 2.9 Electric potential versus displacement curve for dynamic C(T) test of A106 Grade B carbon steel specimen F30-109 at 288 C (550 F)	2-13
Figure 2.10 Load-displacement curves for quasi-static tests of compact specimens from Pipe DP2-F30 (A106 Grade B carbon steel), illustrating crack instabilities with and without partial unloadings	2-13
Figure 2.11 Load-displacement data for compact specimens from Pipe DP2-F30 (A106 Grade B carbon steel), tested at 288 C (550 F)	2-15

Figure 2.12	J-resistance curves for compact specimens, up to the point of the first large crack instability, from Pipe DP2-F30 (A106 Grade B carbon steel) tested at 288 C (550 F)	2-15
Figure 2.13	J versus crack extension for Pipe DP2-A23 (Type 304 stainless steel) specimens tested quasi-statically at 288 C (550 F) at MEA	2-18
Figure 2.14	Load-displacement crack-extension curves for compact specimens from Pipe DP2-A23 (Type 304 stainless steel), tested at 288 C (550 F)	2-19
Figure 2.15	J-resistance curves for compact specimens from Pipe DP2-A23 (Type 304 stainless steel) tested at 288 C (550 F)	2-20
Figure 3.1	Conceptual model of Subtask 1.1 experiment	3-3
Figure 3.2	Subtask 1.1 finite element model	3-4
Figure 3.3	Nomenclature and loading for the application of the Net-Section-Collapse criterion to a pipe with a circumferential surface crack subjected to combined pressure and bending	3-8
Figure 3.4	Nonlinear cracked-pipe section modeling	3-12
Figure 3.5	Basic spring-slider element	3-12
Figure 3.6	Modeling of a multilinear load-displacement curve	3-13
Figure 3.7	Spring-slider model of a crack under reversed loading	3-13
Figure 3.8	Typical moment-rotation behavior of multilinear spring-slider crack model	3-14
Figure 3.9	IPIRG Subtask 1.1 cracked-pipe-section moment-rotation curve for carbon steel surface-cracked-pipe experiment	3-17
Figure 3.10	Subtask 1.1 test section moment for the carbon steel surface-cracked-pipe analysis	3-17
Figure 3.11	Subtask 1.1 actuator force for the carbon steel surface-cracked-pipe analysis	3-18
Figure 3.12	Subtask 1.1 inertial mass displacement for the carbon steel surface-cracked-pipe analysis	3-18
Figure 3.13	Subtask 1.1 output moment-rotation curve for the carbon steel surface-cracked-pipe analysis	3-20

Figure 3.14	IPIRG Subtask 1.1 cracked-pipe-section moment-rotation curve for stainless steel through-wall-cracked-pipe experiment	3-20
Figure 3.15	Subtask 1.1 test section moment for the stainless steel through-wall-cracked-pipe analysis	3-21
Figure 3.16	Subtask 1.1 actuator force for the stainless steel through-wall-cracked-pipe analysis	3-21
Figure 3.17	Subtask 1.1 inertial mass displacement for the stainless steel through-wall-cracked-pipe analysis	3-22
Figure 3.18	Subtask 1.1 output moment-rotation curve for the stainless steel through-wall-cracked-pipe analysis	3-22
Figure 3.19	Aerial photo of West Jefferson site	3-24
Figure 3.20	Sketch of test frame with dimensions of specimen	3-25
Figure 3.21	Load frame used for the two Subtask 1.1 displacement-controlled experiments (1.1-7 and 1.1-9)	3-26
Figure 3.22	End view of stainless steel specimen showing Viton® bladder and steel snap rings	3-28
Figure 3.23	Engineering drawing (units mm) of pipe saddle	3-29
Figure 3.24	Rotation instrument mounted to TWC pipe which is being fatigue precracked	3-30
Figure 3.25	LVDT-based rotation device used on Experiments 1.1-7, 1.1-8, and 1.1-9	3-31
Figure 3.26	Plot of d-c electric potential versus crack-mouth-opening displacement for Experiment 1.1-3	3-35
Figure 3.27	View down length of specimen showing vertical track and wheels on mass	3-38
Figure 3.28	Center span bending moment (data divided by 100) and electric potential at Crack Tip A versus crack-mouth-opening displacement (CMOD) for Experiment 1.1-2. (The CMOD data has been numerically filtered using a 10-point running average scheme.)	3-40
Figure 3.29	Post-test photograph of Specimen 1.1-3. The kink angle is 12 degrees and the crack growth is approximately 20 mm (0.8 inch) on each side	3-41

Figure 3.30	Plot of d-c electric potential versus crack-mouth-opening displacement for Experiment 1.1-3	3-42
Figure 3.31	Moment versus crack-mouth-opening displacement for Experiment 1.1-3	3-42
Figure 3.32	This photograph of the fracture surface of Specimen 1.1-4 shows the external scalloped flaw as well as some small striations which are indicative of small incremental crack growth as the specimen was cycled to failure	3-44
Figure 3.33	Center span bending moment versus time for Experiment 1.1-4	3-45
Figure 3.34	Average displacement for East and West actuators versus time for Experiment 1.1-4	3-45
Figure 3.35	Electric potential at crack centerline, as a function of centerline CMOD for Experiment 1.1-5. Also shown is a plot of center span bending moment. (In order to show the bending moment data on same figure, the bending moment data were divided by 100.) The change in slope of the EP versus CMOD data is indicative of crack initiation	3-46
Figure 3.36	Moment versus time for Experiment 1.1-6. (Moment data are the average of the moment data obtained from the strain gages located 25 and 175 mm (1- and 7-inches) from the crack plane)	3-46
Figure 3.37	Moment-time plot for Experiment 1.1-7. Three curves are shown; one based on load cell data, one on inner strain gage data, and one on outer strain gage data	3-48
Figure 3.38	Strain gage locations for Experiments 1.1-7, 1.1-8, and 1.1-9	3-48
Figure 3.39	Centerline d-c EP versus actuator displacement for Experiment 1.1-7	3-50
Figure 3.40	Moment versus actuator displacement for Experiment 1.1-7	3-50
Figure 3.41	Displacement of the two actuators as a function of time for Experiment 1.1-8	3-51
Figure 3.42	Moment-time plot for Experiment 1.1-8	3-51
Figure 3.43	Moment-time plot for Experiment 1.1-9. Three curves are shown. One curve is based on load cell data, one on inner strain gage data, and one on outer strain gage data	3-53
Figure 3.44	Centerline EP versus crack-mouth-opening displacement for Experiment 1.1-9	3-53

Figure 3.45	Moment versus crack-mouth-opening displacement for Experiment 1.1-9	3-54
Figure 4.1	Bar chart showing the ratio of the maximum experimental stress to the NSC predicted stress. (The flow stress definition for this figure is the average of the yield and ultimate strengths of the material.)	4-7
Figure 4.2	Bar chart showing the ratio of the maximum experimental stress to the NSC predicted stress. (The flow stress definition for this figure is 1.15 times the average of the yield and ultimate strengths of the material.)	4-7
Figure 4.3	Bar chart showing the ratio of the maximum experimental stress to the NSC predicted stress. (The flow stress definition for this figure is $3S_m$ for the stainless steel experiments and $2.4S_m$ for the carbon steel experiments.)	4-8
Figure 4.4	Ratio of maximum experimental stress to predicted Net-Section-Collapse stress as a function of the Dimensionless Plastic-Zone Parameter. (The flow stress definition for this figure is the average of the yield and ultimate strengths.)	4-9
Figure 4.5	Bar chart showing the ratio of the maximum experimental stress to the DPZP predicted stress. (The flow stress definition for this figure is the average of the yield and ultimate strengths.)	4-10
Figure 4.6	Ramberg-Osgood fits of stainless steel Pipe A23 tensile test data at 288 C (550 F)	4-13
Figure 4.7	Ramberg-Osgood fits of A106 Grade B Pipe F30 tensile test data at 288 C (550 F)	4-14
Figure 4.8	Schematic of different J-R curve extrapolation methods	4-15
Figure 4.9	Extrapolated J-R curves for stainless steel Pipe A23 at 288 C (550 F) used in NRCPIPE calculations	4-16
Figure 4.10	Extrapolated J-R curves for A106 Grade B Pipe F30 at 288 C (550 F) used in J-estimation scheme calculations	4-17
Figure 4.11	Bar chart showing the ratio of the experimental crack initiation stress to the GE/EPRI and LBB.ENG2 predictions for the Subtask 1.1 through-wall-cracked-pipe experiments	4-20
Figure 4.12	Bar chart showing the ratio of the maximum experimental stresses to the GE/EPRI and LBB.ENG2 predicted stresses for the Subtask 1.1 through-wall-cracked-pipe experiments	4-23

Figure 4.13	Bar chart showing the ratio of the experimental stresses to the SC.TNP predicted stresses for a number of Subtask 1.1 surface-cracked-pipe experiments	4-25
Figure 4.14	R6 Option 1 failure analysis diagram	4-27
Figure 4.15	Bar chart showing the ratio of the calculated elastic stresses at crack initiation to the R6 Revision 3 Option 1 predictions of crack initiation	4-30
Figure 4.16	Bar chart showing the ratio of the calculated elastic stress at maximum stress to the R6 Revision 3 Option 1 predictions of maximum stress	4-30
Figure 4.17	Bar chart showing the ratio of the maximum experimental stress to IWB-3640 predicted stress for the two Subtask 1.1 stainless steel experiments using two definitions of flow stress	4-35
Figure 4.18	Bar chart showing the ratio of the maximum experimental stress to the IWB-3650 predicted stress for the Subtask 1.1 carbon steel experiments using two definitions of flow stress	4-35
Figure 4.19	J-R curve using the η -factor analysis for Experiment 1.1-3. (For comparison, the J-R curves for the companion quasi-static monotonic pipe experiment (4131-5) and a quasi-static C(T) specimen are also included.)	4-41
Figure 4.20	Comparison of experiment with analysis for moment versus time plots of Experiment 1.1-3 (TP304 TWC). [The analysis was valid only up to maximum moment.]	4-43
Figure 4.21	Comparison of experiment with analysis for displacements of the inertial masses for Experiment 1.1-3 (TP304 TWC)	4-44
Figure 4.22	Comparison of experiment with analysis for moment versus rotation for Experiment 1.1-3 (TP304 TWC)	4-45
Figure 4.23	Comparison of experiment with analysis for moment versus time plots of Experiment 1.1-2 (A106B TWC)	4-48
Figure 4.24	Comparison of experiment with analysis for displacements of the inertial masses for Experiment 1.1-2 (A106B TWC)	4-49
Figure 4.25	Comparison of experiment with analysis for rotations at crack plane for Experiment 1.1-2 (A106B TWC)	4-50
Figure 5.1	Effective stress ratio as a function of cycle number for Experiment 1.1-2	5-3

Figure 5.2	J-R curves for the TP304 stainless steel pipe fracture experiments using the η-factor approach, Reference 5.2	5-3
Figure 5.3	Crack-tip-opening-angle data from IPIRG Subtask 1.2 stainless steel pipe experiments, Reference 5.1	5-4
Figure 5.4	Illustration of postulated trends in discrepancy for elastic stress analysis	5-10
Figure 6.1	Comparison of STA pipe system test failure loads with modified Net-Section-Collapse analyses using flaw size prior to dynamic load (From Ref. 6.5)	6-3
Figure B.1.	Net-Section-Collapse criteria	B-4
Figure B.2	Nomenclature and loading system for the application of the Net-Section-Collapse criterion to a pipe with a circumferential through-wall crack	B-5
Figure B.3	Nomenclature and loading for the application of the Net-Section-Collapse criterion to a pipe with a circumferential surface crack subjected to pure bending	B-6
Figure B.4	Nomenclature and loading for the application of the Net-Section-Collapse criterion to a pipe with a circumferential surface crack subjected to combined pressure and bending	B-7

LIST OF TABLES

	<u>Page</u>
Table 2.1 Tensile test results for specimens machined from Pipe DP2-F30, A106 Grade B carbon steel	2-4
Table 2.2 Tensile test results at 288 C (550 F) for specimens machined from Pipe DP2-A23, Type 304 stainless steel	2-7
Table 2.3 Summary of J_i and dJ/da values for Pipe DP2-F30 obtained from compact specimens tested at 288 C (550 F) at Materials Engineering Associates	2-11
Table 2.4 Summary of J_i and dJ/da values for Pipe DP2-F30 obtained from compact specimens tested at 288 C (550 F) at Battelle	2-16
Table 2.5 Summary of J_i and dJ/da values for Pipe DP2-A23 obtained from compact specimens tested at 288 C (550 F)	2-18
Table 3.1 Test matrix for Subtask 1.1	3-2
Table 3.2 IPIRG Subtask 1.1 finite element model input data for pretest design analysis	3-4
Table 3.3 Summary of nonlinear crack stiffness calculations	3-10
Table 3.4 IPIRG Subtask 1.1 minimum system requirements based on pretest design analysis	3-23
Table 3.5 Summary of results from Subtask 1.1 cracked-pipe experiments	3-37
Table 4.1 Summary table showing ratio of maximum experimental stresses to predicted stresses for Subtask 1.1 experiments for the different fracture analysis methods considered	4-2
Table 4.2a Comparison of Net-Section-Collapse analysis predictions with Subtask 1.1 experimental maximum stresses for stainless steel pipe experiments	4-5
Table 4.2b Comparison of Net-Section-Collapse analysis predictions with Subtask 1.1 experimental maximum stresses for carbon steel pipe experiments	4-6

Table 4.3a	Comparison of Dimensionless Plastic-Zone Screening Criterion predictions with Subtask 1.1 experimental maximum stresses for stainless steel pipe experiments	4-11
Table 4.3b	Comparison of Dimensionless Plastic-Zone Screening Criterion predictions with Subtask 1.1 experimental maximum stresses for carbon steel pipe experiments	4-12
Table 4.4	J-estimation scheme predictions of crack initiation stresses for Subtask 1.1 through-wall-cracked-pipe experiments	4-21
Table 4.5.	J-estimation scheme predictions of maximum stresses for Subtask 1.1 through-wall-cracked-pipe experiments	4-21
Table 4.6.	SC.TNP predictions of crack initiation stresses and maximum stresses for Experiments 1.1-5, 1.1-7, and 1.1-9	4-24
Table 4.7	Comparison of R6 Revision 3 Option 1 predictions with elastic stress analysis results for Subtask 1.1 experiments	4-29
Table 4.8a	Comparison of experimental results with IWB-3640 source equation predictions for the two Subtask 1.1 stainless steel pipe experiments	4-33
Table 4.8b	Comparison of experimental results with IWB-3650 source equation predictions for the five Subtask 1.1 carbon steel pipe experiments	4-33
Table 4-9	Comparison of J_i values for η -factor method with J_i values from C(T) specimens for Subtask 1.1 experiments	4-39
Table 4.10	Comparison of dynamic predictions using ANSYS code and nonlinear spring (cracked pipe) element with through-wall-cracked-pipe experimental results	4-46
Table 4.11	Moments calculated by an elastic stress analysis compared with measured moments for the IPIRG Subtask 1.1 pipe-fracture experiments	4-46
Table A-1.	Compact specimens machined from Pipe DP2-F30, A106 Grade B carbon steel (L-C orientation)	A-1
Table A-2.	Compact specimens machined from DP2-A23, Type 304 stainless steel (L-C orientation)	A-1

EXECUTIVE SUMMARY

This is a report of the results from Subtask 1.1 of the International Piping Integrity Research Group (IPIRG) program. The IPIRG Program was an international group program coordinated by the United States Nuclear Regulatory Commission and conducted at Battelle. The principal objective of this program was to evaluate the mechanical behavior of nuclear piping containing flaws and subjected to dynamic loading. Dynamic loading in this report refers to loading rates comparable to those for high amplitude seismic events.

The design of piping systems in nuclear power plants recognizes the existence of both inertial and displacement-controlled loads. Uncertainties in the ability to characterize inertial loads has, in part, led to a prevailing industry design practice that results in inherently stiff piping systems. However, stiff systems are less tolerant of displacement-controlled loads caused by thermal expansion and differential anchor motion. Unfortunately, a vast majority of data on the mechanical behavior of piping has been obtained under displacement-controlled conditions. Pipe experiments conducted under displacement control, unlike inertial control, are relatively straightforward and require only conventional testing equipment. They also can be conducted without the complicating aspects of increased strain rates and cyclic loads that are inherent in inertially loaded experiments conducted by applying transient loads. A critical need exists to develop a better understanding of inertial stresses in piping, and, in particular, to improve the ability to treat inertial stresses in fracture analysis of flawed piping.

The IPIRG Program was formulated to investigate the separate effects of inertial and displacement-controlled stresses on flawed piping as well as the combined effects on representative piping systems. The specific objective of Subtask 1.1 was to develop data necessary to assess analysis methodologies for characterizing the fracture behavior of circumferentially cracked pipe under inertial (time dependent) stresses. The pipe fracture experiments conducted were on nominal 6-inch diameter, Schedule 120 TP304 stainless and A106 Grade B carbon steel pipes. Both through-wall-cracked and surface-cracked-pipe experiments were conducted. A total of eight cracked-pipe experiments were conducted as part of this effort. Six were inertially loaded experiments and two were displacement-controlled experiments. An uncracked-pipe experiment was also conducted to check out the test facility. The objective of the two displacement-controlled experiments was to assess the effect of dynamic loading rate on the fracture behavior of carbon steel surface-cracked pipes and to compare results obtained using the Subtask 1.1 test facility with those from the Subtask 1.2 facility. All pipes were tested under PWR conditions; all specimens contained subcooled water at 288 C (550 F) and under pressure of 15.5 MPa (2,250 psi). The work is applicable to cracked-pipe evaluations for seismic loading conditions, i.e., leak-before-break (LBB) and in-service flaw evaluation criteria for emergency and faulted conditions.

Due to the unique nature of inertially loaded experiments, it was necessary to design and fabricate a special test system for this investigation. The test system uses two computer-controlled servo-hydraulic actuators. The test pipe is mounted horizontally with a large mass attached to each end. Each specimen had a machined flaw in the center and was shaken vertically by the actuators. The computer controls forced a rapidly increasing amplitude in the center of the specimen at 3.5 Hz. The inertia of the large end masses provided the loading. Special instrumentation techniques were devised to obtain the data from these experiments.

The test system was designed using the ANSYS finite element computer program. A special nonlinear element was devised to model the cyclic response of the flawed piping. While these design calculations were intended only to size the test system components (actuators, servovalves, and accumulators) an additional benefit was that these analyses also provided predictions of the response of the flawed piping to inertial loads. This methodology was essential to the design of the pipe system experiments in Subtask 1.3 of the IPIRG-1 program.

The inertial experiments were analyzed using a number of predictive fracture analysis methods—Net-Section-Collapse analysis, Dimensionless Plastic-Zone Parameter criteria, and various J-estimation schemes, i.e., GE/EPRI and LBF ENG2 for through-wall-cracked-pipe experiments and SC.TNP for surface-cracked-pipe experiments. These analysis procedures were relatively accurate and generally underpredicted the maximum bending stress. The one exception was the Net-Section-Collapse (NSC) predictions of the carbon steel experiments. This is to be expected since the NSC analysis is a limit-load analysis which is probably inappropriate for lower toughness materials such as this carbon steel. For such materials, an elastic-plastic fracture analysis method, such as one of the J-estimation schemes, is probably a more appropriate method.

In addition, the results were compared with various fracture prevention design criteria, i.e., R6 Revision 3 Option 1 for austenitic and ferritic piping, ASME Section XI IWB-3640 for austenitic piping, and IWB-3650 for ferritic piping. For the R6 method the predicted stresses were compared with the elastically calculated stresses instead of experimentally determined stresses since the R6 analysis uses elastically calculated stresses for its assessment of a defect. It was found that the R6 Option 1 method consistently underpredicted the elastically calculated stresses.

In comparing the experimental stresses with the predictions from the ASME Section XI criteria it was found that the IWB-3640 source equations slightly underpredicted the maximum experimental stresses of the austenitic experiments if the ASME Code definition of flow stress ($3S_m$) was used. This was true for a stainless steel pipe material that had strength properties close to the ASME Section III values. This assessment did not include the ASME Code safety factors of 1.39 or 2.78. It was shown for the ferritic criteria, the IWB-3650 source equations, that the approach significantly underpredicted the maximum experimental stresses.

The pipe experiments in this subtask were conducted on a relatively small diameter pipe (6-inch nominal diameter). The effects of toughness on failure loads relative to the limit-load of the pipe are known to be more sensitive for large diameter pipe. Hence, the effects of toughness variations on the crack initiation and maximum loads in these relatively small diameter pipe experiments were expected to be small. The toughness was evaluated directly from these pipe experiments for the purpose of assessing if there may be a consistent trend of toughness from the pipe experiments with different flaw geometries, loading histories, and materials relative to the standard C(T) test data that are typically used for evaluation of larger diameter pipe that would be more sensitive to toughness changes. The findings showed that the surface-crack geometry tended to increase the apparent toughness, relative to the C(T) specimen toughness, unless the loading had a negative load ratio. The carbon steel, quasi-static, surface-cracked-pipe J_i value was much lower than expected and did not follow the anticipated trend, possibly because this length of pipe exhibited much more scatter in its properties than other nuclear pipe tested in the past.

In addition to comparing the experimental results with these fracture assessment criteria, the experimentally determined stresses were also compared with the stresses calculated from a linear-elastic stress analysis. For the Subtask 1.1 experiments, with relatively large cracks and little

nonlinear behavior, the ratio of the calculated linear-elastic stress to the experimental stress was less than 2. This was significantly less than that reported in the EPRI/NRC Piping and Fitting Dynamic Reliability Program for uncracked pipe. It seems certain that if smaller flaws, e.g., LBB size through-wall cracks or smaller surface cracks, had been used in the Subtask 1.1 experiments, then elastic stress analysis would give much larger values than the experimental values reported in this effort.

One of the most important observations that can be made from these experiments is that once maximum load was achieved there were very few additional cycles before the pipe essentially reached complete failure. For five of the six cracked-pipe inertial experiments there were only 2 or 3 cycles after maximum load before a DEGB condition was reached. The implication is that for seismic applications the stability of flawed pipe under pure inertial loading, or inertial with dead-weight and pressure stresses, should be considered as load-controlled rather than displacement-controlled for typical pipe fracture analyses. However, these were relatively small diameter pipe tests, and larger pipe might be able to tolerate more cycles under pure inertial loading.

ACKNOWLEDGEMENTS

The IPIRG Program was an international group program coordinated by the U.S. Nuclear Regulatory Commission's Materials Engineering Branch of the Office of Nuclear Regulatory Research under Contract No. NRC-04-86-106 to Battelle. Mr. M. Mayfield was the USNRC program manager. Dr. R. Schmidt was the Battelle program manager.

The members of the IPIRG Program and their representatives to the IPIRG Technical Advisory Group (TAG) were:

Canada

- AECB^(a)
- Ontario Hydro

Dr. A. Omar^(b), Dr. B. Jarman, and Mr. K. Pereira
Mr. M. Kozluk

France

- CEA^(a)
- EDF
- Framatome

Mr. Ph. Jamet^(b), Ms. F. Gantenbein, and Mr. M. Petit
Mr. C. Faigy and Mr. P. Le Delliou
Dr. Ph. Gilles and Dr. S. Bhandari

Italy

- ENEA^(a)

Dr. C. Maricchiolo^(b) and Dr. P. Milella

Japan

- CRIEPI^(a,c)

Dr. K. Kashima^(b) and Mr. K. Kuwabara

Republic of China

- AIT^(a)
- ROC-AEC
- INER

Mr. Y. H. Cheng^(b) and K.-S. Chao, Taiwan Power Co.
Mr. R. Y. Wu
Dr. K. Ting

Sweden

- SKI^(a)

Dr. B. Brickstad^(b) and Dr. F. Nilsson, AB Statens
Anläggningsprovning

Switzerland

- HSK^(a)
- Kernkraftwerk Union

Mr. H. Njo^(b)
Dr. D. Burns

(a) Contractual organization.

(b) TAG representative at end of program.

(c) Other Japanese members included, Mitsubishi, Toshiba, Hitachi, IHI, and 13 electric utilities.

United Kingdom

- Nuclear Electric^(a,c)
- NII

Dr. B. J. Darlaston^(b)
Mr. P. Harrop

United States

- USNRC-RES^(a)
- USNRC-NRR
- EPRI^(a)

Mr. M. Mayfield^(b)
Mr. K. Wichman^(b)
Mr. J. Gilman^(b) and Dr. D. Norris

We would like to express our appreciation for the support and interest of the IPIRG members in this program.

During the course of this program, there were also contributions by the following visiting scientists; Mr. Ph. Gilles (Framatome, France), Mr. K.-S. Chao (Taiwan Power Company, Taiwan), and Dr. C. Maricchiolo (ENEA, Italy). We would like to thank them for their assistance.

Researchers and technicians at Battelle who also contributed to this work are; J. Ahmad, J. Anthony, F. Brust, C. Cundiff, N. Frey, J. Garrabrant, R. Gertler, D. Guerrieri, P. Held, D. Jones, J. Kramer, M. Landow, P. Mincer, M. Oliver, D. Rider, J. Ryan, D. Shoemaker, P. Vieth and J. Wood. Quality assurance review was provided by E. Fromm and D. Lozier. We would also like to acknowledge the assistance of Mrs. G. Walter and Mrs. B. Blanton in coordinating this report.

Additional thanks to C. Baxley and the entire Battelle machine shop for their diligence and efforts in all that was asked of them.

(a) Contractual organization.

(b) TAG representative at end of program.

(c) Formerly CEGB.

NOMENCLATURE

1. SYMBOLS

A	Coefficients used in ASME Section XI ferritic pipe criteria
A'	Distance between inner and outer load points in four-point bending
a	Half average crack length of through-wall crack in a pipe which includes a_o
a_i	C(T) specimen initial crack
a_o	Original half crack length of machined flaw and fatigue precrack if applicable
C'	Statistical fit parameter
C	Machine compliance
c	Distance from neutral axis to outer fiber
c', c''	Constants in J-R curve power-law curve fit equation
D	Nominal pipe diameter
D_o	Outer pipe diameter
d	Surface crack depth
E	Young's modulus
F	Applied force (actuator)
F_i	Load at i-th increment of a static four-point bend test
F_{slide}	Force at which a spring-slider element slides in a FEM
f_p	Ratio of bending moment in pressurized versus unpressurized pipe
F_1, F_2, F_3	Forces needed to cause sliding in multilinear spring-slider model
F_{T1}, F_{T2}	Total forces in a multilinear spring-slider model
h_1^B	GE/EPRI influence function for through-wall-cracked pipe under pure bending
h_1^T	GE/EPRI influence function for through-wall-cracked pipe under pure tension
$\hat{h}$	Suggested GE/EPRI influence function for through-wall-cracked pipe under combined bending and tension

h_1, h_2, h_3, h_4	Functions tabulated in GE/EPRI method
I	Moment of inertia
I'	Node in spring-slider model
J	J-integral fracture parameter
J'	Node in spring-slide model
$J-R$	J-integral resistance (curve)
J_D	Deformation J-integral
J_e	Elastic component of J
J_M	Modified J-integral
J_i	J-integral at crack initiation but not necessarily a valid J_{Ic} by ASTM E813-81
J_p	Plastic component of J-integral
J_{Ic}	Plane strain J at crack initiation by ASTM E813
$[K]$	Stiffness matrix
K_a	Applied linear elastic stress intensity factor
K_T	Toughness ratio in R6 method
K_1, K_2, K_3	Spring constants in multilinear spring-slider model
K_{T1}, K_{T2}	Total spring constants in multilinear spring-slider model
K_{Im}, K_{Ib}	Membrane and bending stress intensity factors
l	Inner load-point span
L	Outer load-point span
L_r	Load ratio in R6 method
m, m'	J-R curve power-law exponents
M	Bending moment
M_e	Elastic moment

M_{eq}	Equivalent moment
M_o	Limit moment based on σ_o
M_{test}	Moment from the test
n	Ramberg-Osgood strain-hardening exponent
p	Pressure
P	Total failure stress
P_o	Limit-load corresponding to reference stress σ_o
P_m	Primary membrane stress in ASME Code
P_{nsc}	Net-Section-Collapse predicted tension and bending stress
q	Displacement vector in finite element model
R	Stress ratio
R_i	Inside radius
R_m	Mean radius
R_o	Outer surface radius
S	Pipe length
S'	Span of torque bar/LVDT rotation device
S_m	ASME design stress
S_r	Stress ratio in R6 method
T	Temperature
t	Thickness of pipe; t also denotes time but content always makes the use clear
U	Direct current electric potential
w	Weight of pipe and water in pipe
W	C(T) specimen width
Z	Stress multiplier for elastic-plastic analyses

α	Ramberg-Osgood parameter
α'	Damping factor in dynamic analyses
β'	Damping factor in dynamic analyses
β	Angle from bottom of pipe to neutral axis
γ	Function in pipe η -factor J-integral expression
γ_s	Specific weight of pipe steel
γ_w	Specific weight of water
$\dot{\delta}$	Time derivative of the displacement
δ	Displacement at load-points
Δ	Displacement output of LVDT for rotation measurements
Δa	Increment of crack growth
$\dot{\epsilon}$	Strain Rate
ϵ_0	Ramberg-Osgood reference strain
ϵ_B	Longitudinal strain at bottom of pipe
ϵ_f	Elastic strain at flow stress
ϵ_T	Longitudinal strain at top of pipe
η	Geometric function used in calculating J from load-displacement data
θ	Half crack angle of crack in a pipe
ν	Poisson's ratio
ϕ	Half rotation angle of pipe
ϕ_p^c	Plastic component of rotation due to the crack
σ	Stress
σ_a	Axial stress
σ_b	Bending stress

σ_{bNSC}	Bending stress predicted by Net-Section-Collapse analysis
σ_b	Longitudinal stress at bottom of pipe
σ_f	Flow stress
σ_{nsc}	Stress predicted by Net-Section-Collapse analysis
σ_o	Ramberg-Osgood reference stress
σ_T	Longitudinal stress at top of pipe
σ'_L	Limit stress based on flow stress for combined bending and tension
σ'_T	Applied tension stress due to pressure
σ_u	Ultimate strength
σ_y	Yield strength
Ψ	Constant in dynamic forcing function
ω	Angular velocity in radians per second

2. ACRONYMS AND INITIALISMS

a-c	Alternating current
ASME	American Society of Mechanical Engineers
ASTM	American Society of Testing and Materials
CEGB	Central Electricity Generating Board (U.K.), currently Nuclear Electric (N.E.)
CTOA	Crack tip opening angle
CMO	Crack mouth opening
CMOD	Crack-mouth-opening displacement
C(T)	Compact (tension) specimen
CS	Carbon steel
d-c EP	Direct-current electric potential
DEGB	Double-ended guillotine break

DSA	Dynamic strain aging
DP2	Degraded Piping Program - Phase II
DPZP	Dimensionless Plastic-Zone Parameter
DYN	Dynamic
EDM	Electric discharge machine
EP	Electric potential
EPRI	Electric Power Research Institute (U.S.)
GE/EPRI	Circumferential TWC pipe J-estimation scheme
GE	General Electric
IPIRG	International Piping Integrity Research Group
ISC	Internal surface crack
LBB	Leak-before-break
LLD	Load-line displacement
LVDT	Linear variable differential transformer
LWR	Light water reactor
L-C	Longitudinal-circumferential orientation
MEA	Materials Engineering Associates
MTS	Material Testing Systems
NPS	Nominal pipe size
NSC	Net-Section-Collapse
OBE	Operating Basis Earthquake
PWR	Pressurized water reactor
RVDT	Rotary variable differential transformer
QS	Quasi-static

SAW	Submerged arc weld
SC	Surface crack
SMAW	Shielded metal arc weld
SS	Stainless steel
SSE	Safe shut-down earthquake
STA	Science and Technology Agency (Japan)
TWC	Through-wall crack
USNRC	United States Nuclear Regulatory Commission
XSC	External surface crack

1. INTRODUCTION

This report covers results for Subtask 1.1 of the International Piping Integrity Research Group (IPIRG) Program. The IPIRG Program was an international group program coordinated by the United States Nuclear Regulatory Commission (USNRC) and conducted at Battelle. The principal objective of the IPIRG program is to evaluate the mechanical behavior of nuclear piping containing flaws and subjected to dynamic loading.

The specific results reported in this document are for experiments and analyses of circumferentially cracked pipes that were subjected to predominately inertial loading. This work is applicable to cracked-pipe evaluations for seismic loading conditions, i.e., leak-before-break (LBB) and in-service flaw evaluation criteria for operating basis earthquake (OBE) loading as well as for safe shut-down earthquake (SSE) loading conditions.

1.1 Background

In LBB analyses, a postulated flaw is assumed to exist. In many LBB analyses, it is assumed that the flaw becomes a through-wall crack, and that the through-wall crack can be detected by leakage at normal operating stresses in advance of a large dynamic load rupture. It is also necessary to show that the flaw will remain stable under various events causing dynamic loadings such as a safe-shutdown-earthquake (SSE) condition. Safety factors are applied to the leakage detection capability, loads and flaw size (Ref. 1.1). Consequently, LBB analyses generally involve assessments of the stability of circumferential through-wall-cracked pipe subjected to dynamic cyclic loads.

For in-service flaw assessment criteria, the stresses of concern can be either quasi-static (normal and test conditions) or dynamic (emergency or faulted conditions). However, the flaw evaluation procedures generally involve the assessment of surface cracks. These surface cracks can have either axial, circumferential, or helical flaw orientations. Such assessments can involve the evaluation of surface-cracked pipe under dynamic cyclic loads. In the United States, the ASME Section XI code includes procedures to assess flaws in austenitic (Ref. 1.2) or ferritic piping (Ref. 1.3). Several other countries use different assessment methods including the R6 method, Ref. 1.4.

Many flawed-piping research programs have been conducted in the United States (Refs. 1.5 to 1.9) and internationally (Refs. 1.10 to 1.14). However, most of these programs involved quasi-static loading of cracked pipe, rather than the dynamic seismic loading that is more realistic for LBB and in-service flaw assessments. The IPIRG Program was created to make fracture evaluations of piping under simulated seismic loading.

1.2 Overview of IPIRG Program

The IPIRG Program was initiated by the NRC with the objective to develop data needed to verify engineering methods for assessing the integrity of nuclear power plant piping that contains defects. The work within the IPIRG program is broken down into five tasks:

- Task 1 LBB Verification Under Simulated Seismic/Dynamic Loading,
- Task 2 Experimental Pipe Fracture and Material Property Data Base Development,
- Task 3 Fracture of Piping Containing High Energy Fluids,
- Task 4 Resolution of Unresolved Issues from the Degraded Piping Program and IPIRG Research Tasks, and
- Task 5 Information Exchange Seminars and Program Administration.

Within Task 1 there are three subtasks. These are:

- Subtask 1.1 Stability of Cracked Pipe Under Inertial Loading,
- Subtask 1.2 Stability of Cracked Pipe Under Seismic/Dynamic Displacement-Controlled Stresses, and
- Subtask 1.3 Crack Stability in a Representative Piping System Under Combined Inertial and Seismic/Dynamic Displacement-Controlled Stresses.

This report describes the results obtained from Subtask 1.1 of the IPIRG program.

1.3 Specific Objective of Inertially Loaded Pipe Fracture Subtask

The specific objective of Subtask 1.1 was to develop data necessary to assess analysis methodologies for circumferentially cracked pipe under inertial (time-dependent) stresses. One of the main questions is how to treat inertial stresses in a fracture analysis. Current engineering fracture analyses consider the stresses as either displacement-controlled or load-controlled. Once maximum load is passed, load-controlled stresses cause the crack to be less stable than displacement-controlled stresses. This is illustrated in Figure 1.1 which involves center-cracked stainless-steel-plate experiments in Reference 1.6.

Limited analytical evaluations were undertaken to compare the predicted loads with the experimental results. Of importance to IPIRG Subtask 1.3 was the verification of a nonlinear spring concept to conduct dynamic analyses of the representative cracked-pipe system experiments. This analysis, as well as other quasi-static analyses such as, the Net-Section-Collapse analysis and various elastic-plastic fracture analyses and the ASME code procedures

(a) Applied load versus crack-opening displacement curve for a center-cracked plate specimen with a 76.2 mm (3-inch) central notch under interrupted fatigue loads (displacement control)

(b) Applied load versus crack-opening-displacement curve for a center-cracked plate specimen with a 76.2 mm (3-inch) central notch under interrupted fatigue loads (load control)

Figure 1.1. Stability of center-cracked Type 304 stainless steel plates under cyclic displacement-control and cyclic load-control conditions (from Ref 1.6)

11.1-8/89-F1.1

were used to predict the experimental loads, moments and rotations of the pipes in the cracked-pipe experiments.

In addition to the pipe fracture experiments, limited material property data were also developed at quasi-static and dynamic rates for tensile tests and C(T) tests used to calculate the material's fracture resistance using the J-integral parameter.

Finally, it is also important to note that the results of these experiments involve two extra parameters, higher strain rates and cyclic loading interactions with ductile tearing, that were not considered in the quasi-static pipe fracture experiments such as those in References 1.5 to 1.14. These two parameters were evaluated as separate effects in IPIRG Subtask 1.2, Refs. 1.15 and 1.16.

1.4 References

- 1.1 "Evaluation of Potential for Pipe Breaks", prepared by NRC Pipe Break Task Group, Report NUREG-1061, Vol. 3, November 1984.
- 1.2 American Society of Mechanical Engineers Boiler and Pressure Vessel Code, See Section XI Article IWB-3640, Edition July 1989.
- 1.3 American Society of Mechanical Engineers Boiler and Pressure Vessel Code, See Code Case N-463, Edition July 1989.
- 1.4 Milne, I., Ainsworth, R. A., Dowling, A. R., and Stewart, A. T., "Assessment of the Integrity of Structures Containing Defects", R/H/R6-Rev. 3, Published by Central Electric Generating Board, England, May 1986.
- 1.5 Wilkowski, G. M., and others, "Degraded Piping Program - Phase II", Summary of Technical Results and Their Significance to Leak-Before-Break and In-Service Flaw Acceptance Criteria, March 1984-January 1989, by Battelle Columbus Division, NUREG/CR-4082, Vol. 8, March 1989.
- 1.6 Kanninen, M. F., and others, "Instability Predictions for Circumferentially Cracked Type 304 Stainless Steel Pipes Under Dynamic Loadings", Final Report on EPRI Project T118-2, by Battelle Columbus Laboratories, EPRI Report Number NP-2347, April 1982.
- 1.7 Eiber, R. J., Maxey, W. A., and Duffey, A. R., "Investigation of the Initiation and Extent of Ductile Pipe Rupture", Battelle Memorial Institute Report, BMI-1908, 1971.
- 1.8 Vassilaros, M. G., Hays, R. A., Gudas, J. P., and Joyce, J. A., "J-Integral Testing Instability Analysis for 8-Inch Diameter ASTM A106 Steel Pipe", U.S. David W. Taylor Naval Ship Research and Development Laboratory, NUREG/CR-2347, April 1984.

- 1.9 Tang, H. T., Duffey, R. B., Sing, A., and Bausch, P., "Experimental Investigation of High Energy Pipe Leak and Rupture Phenomena", in ASME Special Publication PVP Volume 98-8, Fracture, Fatigue and Advanced Mechanics, June 1985.
- 1.10 Takumi, K., "Results of the Japanese Carbon Steel Pipe Fracture Program", in LEAK-BEFORE-BREAK: Further Developments in Regulatory Policies and Supporting Research, NUREG/CP-0109, pp 13-32, February 1990.
- 1.11 Darlaston, B. J. L. and Harrison, R. P., "Ductile Failure of Thin-Walled Pipes with Defects Under Combinations of Internal Pressure and Bending", 3rd International Conference on Pressure Vessel Technology, Part II, Materials and Fabrication, pp 669-676, April 1977.
- 1.12 Maricchiolo C. and Milella, P. P., "Fracture Behavior of Carbon Steel Pipes Containing Circumferential Cracks at Room Temperature and 300 C", Nuclear Engineering and Design, 111, pp 35-46, 1989.
- 1.13 Faigy, C., Jamet, P., and Bhandari, S. "Developments in Leak Before Break Approach in France", NUREG/CP-0092, pp 69-82, March 1988.
- 1.14 Sturm, et al., "Fracture Initiation and Fracture Opening under Light Water Reactor Conditions", Nuclear Engineering and Design, Vol. 49, pp 117-134, 1978.
- 1.15 Scott, P., Kramer, G., Veith, P., Francini, R., and Wilkowski, G., "The Effect of Dynamic and Cyclic Loading During Ductile Tearing on Circumferentially Cracked Pipe – Experimental Results", ASME PVP Vol. 280, June 1994, pp 207-220.
- 1.16 Wilkowski, G., Kramer, G., Vieth, P., Francini, R., and Scott, P., "The Effect of Cyclic Loading During Ductile Tearing on Circumferentially Cracked Pipe – Analytical Results", ASME PVP Vol. 280, June 1994, pp 221-240.

2. MATERIAL CHARACTERIZATION EFFORTS

This section of the report addresses the material selection, quasi-static and dynamic tensile test results, the quasi-static and dynamic fracture toughness test results using C(T) specimens, and discusses the effect of quasi-static versus dynamic testing rates on the materials evaluated in Subtask 1.1. The dynamic testing rates used in these efforts correspond to high amplitude, low frequency, seismic rates.

2.1 Material Selection

Three criteria were employed for the selection of materials to be used for the IPIRG pipe experiments. The first was that two materials of different toughness be chosen, one of sufficiently high toughness to cause limit-load conditions to prevail in the pipe experiments, and the other of sufficiently low toughness to cause the occurrence of elastic-plastic fracture. The second criterion was to choose materials that were pedigreed in the Degraded Piping Program, so that data from quasi-static monotonically-loaded pipe experiments were available for comparison with the data being developed in this program. The third criterion was to select relatively small diameter pipe so as not to significantly increase the experimental pipe fracture test cost.

A stainless steel pipe was selected for the high toughness material. This was a nominal 6-inch-diameter, Schedule 120, TP304 stainless steel pipe that was procured in the Degraded Piping Program from a cancelled nuclear plant. Several pipes were available from the same heat of material. These pipes have Battelle designation numbers DP2-A23 to DP2-A27. The material property data were determined from Pipe DP2-A23. The IPIRG pipe test specimens were made from Pipe DP2-A27. (A tensile test from Pipe DP2-A27 was conducted at 288 C (550 F) and found to be virtually identical to Pipe DP2-A23 data. The DP2-A27 tensile test data are not given in this report.) These pipes were manufactured by Babcock and Wilcox (heat number M2783). These pipes were used in the following Degraded Piping Program circumferentially-cracked-pipe experiments at 288 C (550 F):

<u>Experiment No.</u>	<u>Crack Type</u>	<u>Loading</u>
4131-5	Through-wall	Four-point bending
4121-1	Through-wall	Pressure to failure
4131-1	Through-wall	Pressure and four-point bending
4121-3	Internal surface	Pressure to failure
4112-3	Internal surface	Four-point bending
4115-8	Internal surface	Compliant four-point bending
4115-9	Internal surface	Compliant four-point bending
4131-6	Internal surface	Four-point bending
4131-2	Internal Surface	Pressure and four-point bending
4113-1	Complex ^(a)	Four-point bending
4113-2	Complex	Four-point bending
4114-2	Complex	Compliant four-point bending

(a) A complex crack is a long internal surface crack that penetrates the pipe thickness over a short length.

A carbon steel pipe was selected for the low toughness material. This was a nominal 6-inch-diameter, Schedule 120, A106 Grade B pipe that was procured in the Degraded Piping Program from a pipe supply warehouse. Several lengths of pipe from the same heat were procured. These pipes have Battelle designation numbers DP2-F30 and DP2-F31, and were obtained from a supplier, Valley Steel Products Company (heat number UBX). This particular pipe has a sufficiently low toughness to fail at less than limit-load conditions for crack geometries used in this effort, even for this relatively small pipe diameter. The material property data were taken from Pipe DP2-F30. The IPIRG pipe test specimens were made from Pipe DP2-F30. These pipes were used in the following Degraded Piping Program circumferentially-cracked-pipe experiments at 288 C (550 F):

<u>Experiment No.</u>	<u>Crack Type</u>	<u>Loading</u>
4112-6	Surface	Four-point bending
4113-5	Complex	Four-point bending
4113-6	Complex	Four-point bending
4114-1	Complex	Compliant four-point bending

2.2 Tensile Test Results

Tensile tests were conducted at both Battelle and Materials Engineering Associates (MEA). The MEA tests and some of the quasi-static tests at Battelle were conducted in conjunction with the Degraded Piping Program. Tests at MEA were conducted at quasi-static rates and used round-bar, threaded-end specimens having a gage-section diameter of 5.08 mm (0.2 inch) and a reduced section length of 25.4 mm (1.0 inch).

Dynamic tests at Battelle were conducted on flat, pin-loaded specimens having a width of 6.35 mm (0.25 inch), thickness of 3.18 mm (0.125 inch), and length of 25.4 mm (1.0 inch) in the gage section. Quasi-static tests at Battelle employed both the flat, pin-loaded specimens just described, and round-bar, threaded-end specimens having a gage-section diameter of 6.35 mm (0.25 inch) and a gage length of 25.4 mm (1 inch). In all tests at Battelle and MEA, the tensile axis of the specimen was parallel to the pipe axis.

The dynamic tensile tests were conducted in a servohydraulic testing machine at strain rates of nominally 1 and 10 s⁻¹. Strain was measured using an optical extensometer (Optron Model 511) which followed flags located at opposite ends of the gage section. In some of the tests, no useful strain data were obtained; in those tests, only the ultimate tensile strength and the fracture elongation were reported.

2.2.1 Tensile Properties of Pipe DP2-F30, ASTM A106 Grade B Carbon Steel

The tensile properties for Pipe DP2-F30 were studied over a wide range of temperatures and strain rates in an attempt to characterize the steel's susceptibility to dynamic strain aging (DSA). DSA is a phenomenon in which metallurgical aging occurs simultaneously with

plastic straining (Ref. 2.1). In carbon steels, it produces several unusual effects, including: (1) the tensile strength in quasi-static tests at 288 C (550 F) may be greater than at room temperature, (2) at 288 C (550 F), increasing the rate of straining may lower the tensile strength, and (3) serrations may appear on the stress-strain curve at certain temperatures and strain rates.

Table 2.1 is a summary of the tensile tests conducted on specimens machined from Pipe DP2-F30. A graph of tensile properties as a function of test temperature and strain rate is shown in Figure 2.1. Of particular interest, with respect to the dynamic pipe fracture experiments, is the effect of high strain rates at 288 C (550 F). At strain rates of approximately 1 and 10 s⁻¹, both the ultimate tensile strength and the fracture elongation were significantly lower than at quasi-static testing rates. Work conducted by Manjoine (Ref. 2.2) on a different carbon steel showed similar trends and also revealed that the elevated temperature tensile strength in steels susceptible to DSA does not drop continuously with increasing strain rate; instead, at a sufficiently high strain rate, the trend reverses, resulting in increasing strength with further increases in strain rate.

Figure 2.2 shows engineering stress-strain curves for three DP2-F30 tensile specimens tested at 288 C (550 F) at three different strain rates. True stress-strain curves up to the onset of necking are shown in Figure 2.3 for the same three specimens. Note in both Figure 2.2 and Figure 2.3 that the stress-strain curve at the lowest strain rate was smooth and rose higher than the other two curves. The curve for the specimen tested at an intermediate strain rate contained pronounced serrations, which are typical of steels susceptible to DSA when tested at certain temperatures and strain rates. Finally, the curve for the specimen tested at the highest strain rate displayed only modest serrations and rose to a lower stress level than did either of the other two specimens.

Summarizing, the tensile properties of carbon-steel pipe DP2-F30 were found to be influenced significantly by both temperature and strain rate. At a typical nuclear piping service temperature of 288 C (550 F), increasing the strain rate from 10⁻⁴ to 10 s⁻¹ lowered the ultimate strength approximately 25 percent and lowered the fracture elongation approximately 35 percent. Although this type of behavior is not observed in most metallic materials, it was observed here because of the susceptibility of this steel to DSA.

2.2.2 Tensile Properties of Pipe DP2-A23, Type 304 Stainless Steel

Table 2.2 is a summary of the tensile tests conducted at 288 C (550 F) on specimens machined from Pipe DP2-A23. The round-bar specimens tested at two different laboratories at quasi-static rates showed excellent agreement in results. However, a single flat specimen tested at a quasi-static rate at Battelle exhibited both lower strength and lower elongation than did the round-bar specimens. While the smaller cross-sectional area of the flat specimen could account for a portion of the observed ductility reduction (Ref. 2.3), the only logical explanation for the lower strength of the flat specimens is material variability within the same heat of steel. Because the flat specimens gave somewhat different results than did the round-bar specimens, any comparison between dynamic tests (which employed flat specimens) and quasi-static tests should be based on flat-specimen tests.

**Table 2.1. Tensile test results for specimens machined from
Pipe DP2-F30, A106 Grade B carbon steel**

(Tensile axis was parallel with pipe axis; flat, pin-loaded specimens were used, except as noted)

Specimen Identification No.	Test Temperature,		Strain Rate, s^{-1}	0.2 Percent Offset Yield Strength,		Ultimate Tensile Strength,		Reduction in area, percent	Elonga- tion, percent in 25.4 mm (1.0 inch)
	C	F		MPa	kai	MPa	kai		
ZP15-1L ^(a)	22	72	4×10^{-5}	314	45.6	520	75.4	62.2	38.6
ZP15-2L ^(a)	149	300	4×10^{-5}	308	44.7	628	91.1	37.6	23.6
F30-122	149	300	8.7	373	54.1	521	75.6	(c)	24.8
F30-127	149	300	11.7	361	52.3	519	75.2	(c)	27.4
ZP15-3L ^(a)	204	400	4×10^{-5}	341	49.4	660	95.7	33.5	16.0
F30-123	232	450	7.6	294	42.6	481	69.8	(c)	22.8
F30-126	232	450	10.4	332	48.1	479	69.4	(c)	24.1
ZP15-4L ^(a)	288	550	4×10^{-5}	320	46.4	621	90.0	34.4	24.0
F30-5 ^(b)	288	550	3×10^{-4}	342	49.6	647	93.9	37.1	26.8
F30-6 ^(b)	288	550	3×10^{-4}	361	52.3	650	94.3	38.4	25.6
F30-102	288	550	4×10^{-4}	N.D. ^(d)	N.D.	654	94.8	(c)	24.5
F30-104	288	550	4×10^{-4}	294	42.6	599	86.9	(c)	26.0
F30-101	288	550	1.6	N.D.	N.D.	562	81.5	(c)	21.1
F30-100	288	550	3.4	279	40.4	550	79.7	(c)	21.6
F30-106	288	550	9.1	N.D.	N.D.	494	71.7	(c)	14.5
F30-105	288	550	9.8	N.D.	N.D.	505	73.3	(c)	17.0
F30-103	288	550	10.5	N.D.	N.D.	479	69.4	(c)	17.0
F30-107	288	550	11.6	356	51.7	479	69.5	(c)	17.0
ZP15-5L ^(a)	343	650	4×10^{-5}	315	45.7	586	85.0	41.5	35.2
F30-120	343	650	6.3	305	44.3	547	79.3	(c)	18.1
F30-128	343	650	9.7	368	53.3	546	79.2	(c)	16.7

(a) Round-bar, threaded-end specimens were fabricated and tested at MEA.

(b) Round-bar, threaded-end specimen was used.

(c) Flat specimens were used; reduction of area was not measured.

(d) N.D. = Not determined

Figure 2.1. Effect of strain rate and test temperature on tensile properties of Pipe DP2-F30 (A106 Grade B carbon steel)
I1.2-8/89-F2.1

Figure 2.2. Engineering stress-strain curves for tensile specimens from Pipe DP2-F30 (A106 Grade B carbon steel), tested at several different strain rates at 288 C (550 F)
 I1.2-8/89-F2.2

Figure 2.3. True stress-strain curves for tensile specimens from Pipe DP2-F30 (A106 Grade B carbon steel), tested at several different strain rates at 288 C (550 F)
 I1.2-8/89-F2.3

Table 2.2. Tensile test results at 288 C (550 F) for specimens machined from Pipe DP2-A23, Type 304 stainless steel (Tensile axis was parallel with pipe axis)

Specimen Identification No.	Specimen Type ^(a)	Strain, Rate, s ⁻¹	0.2-Percent Offset Yield Strength		Ultimate Tensile Strength,		Elongation, Percent in 25.4 mm (1.0 inch)	Reduction in Area, percent
			MPa	ksi	MPa	ksi		
A23-1	R	3.7 x 10 ⁻⁴	134	19.4	451	65.4	53.5	73.4
A23-2	R	3.7 x 10 ⁻⁴	128	18.6	447	64.8	53.5	74.5
ZP17-16L ^(b)	R	3.7 x 10 ⁻⁵	147	21.3	448	65.0	51.0	62.6
ZP17-13L ^(b)	R	4.1 x 10 ⁻⁵	145	21.1	453	65.7	57.0	75.0
A23-105	F	5.5 x 10 ⁻⁴	N.D. ^(c)	N.D.	400	58.0	46.0	N.D.
A23-101	F	1.1	N.D.	N.D.	413	59.9	47.0	N.D.
A23-104	F	0.3	N.D.	N.D.	421	61.0	46.0	N.D.
A23-102	F	10.0	N.D.	N.D.	413	59.9	52.0	N.D.
A23-103	F	8.9	N.D.	N.D.	423	61.3	47.5	N.D.
A23-108	F	9.0	N.D.	N.D.	396	57.4	47.0	N.D.

(a) R = Round-bar, threaded-end specimen,

F = Flat, pin-loaded specimen.

(b) Specimen was fabricated and tested at Materials Engineering Associates.

(c) N.D. = Not determined.

Figure 2.4 shows tensile stress-strain curves for flat specimens tested at several different strain rates at 288 C (550 F). The quality of the strain data at strains less than about 2 percent in this series of tests was inadequate to permit measurement of the 0.2-percent offset yield strength or Young's modulus of elasticity. However, the results depicted in Figure 2.5 suggest that the 0.2 percent offset yield strength, had it been measured, would have shown a greater increase with increasing strain rate than did the ultimate tensile strength (the top curve in Figure 2.5). Figure 2.5 shows that the ultimate tensile strength was virtually unchanged by a four-orders-of-magnitude change in strain rate, while the stress to cause 2-percent plastic strain increased by more than 25 percent. From that observation, it follows that the 0.2-percent offset yield strength would likely also be increased more than 25 percent by a similar strain rate increase. By extrapolating a graph of log stress versus log plastic strain to a strain of 0.2 percent, using the data shown in Figure 2.5, the following estimates were made:

Strain Rate, s ⁻¹	Estimated 0.2- Percent Offset Yield Strength,	
	MPa	ksi
6 x 10 ⁻⁴	124	18
1	145	21
10	172	25

Figure 2.4. Engineering stress-strain curves for tensile specimens from Pipe DP2-A23 (Type 304 stainless steel), tested at several different strain rates at 288 C (550 F)
 I1.2-8/89-F2.4

Figure 2.5. Strength versus strain rate at selected amounts of plastic strain for tensile specimens from Pipe DP2-A23 (Type 304 stainless steel), tested at 288 C (550 F)
 I1.2-8/89-F2.5

In summary, increasing the strain rate at 288 (550 F) by four orders of magnitude raised the yield strength of Pipe DP2-A23 significantly, but had only a modest effect on ultimate tensile strength and fracture elongation.

2.3 Fracture Toughness Results

J-resistance tests at 288 C (550 F) on side-grooved 0.5T compact specimens machined from Pipes DP2-F30 (A106 Grade B carbon steel) and DP2-A23 (Type 304 stainless steel) in the L-C orientation were conducted at both Materials Engineering Associates and Battelle. The MEA tests and some of the quasi-static tests at Battelle were conducted in conjunction with the Degraded Piping Program. MEA conducted only quasi-static tests, whereas Battelle conducted both quasi-static and dynamic tests.

Quasi-static tests conducted at MEA employed the unloading compliance method to estimate crack extension at regular intervals during the test. From the measured loads and load-line displacements and the calculated crack lengths, values of J were calculated for each unloading point. The method employed by MEA to obtain J at crack initiation (J_i) was identical to those described in ASTM E813-81, except that the J- Δa graphs submitted by MEA to Battelle employed Modified-J (J_M) values (Ref. 2.4) rather than Deformation-J (J_D) values. For use in this report, MEA's data were reanalyzed at Battelle in the manner described in ASTM E1152-87 to provide Deformation-J (symbolized in this case simply as J) values. These values of J were used in constructing J-resistance curves.

Quasi-static and dynamic tests conducted at Battelle employed the direct-current electric potential (d-c EP) method to monitor crack initiation and growth (Ref. 2.5). For the dynamic tests on carbon steel specimens, the direct current applied was about 80 amperes, compared with about 10 amperes applied to the stainless steel specimens and the carbon steel specimens tested at quasi-static rates. The larger current was used in the carbon steel dynamic tests to minimize the effect of a voltage pulse generated within carbon steel specimens when they are subjected to rapid loading. The voltage pulse phenomenon and the method for minimizing its effects were investigated in a separate Battelle-funded study.

Displacement rates in quasi-static tests were selected to cause crack initiation in 5 to 20 minutes. In dynamic tests, the displacement rate was selected to cause crack initiation in approximately 0.2 seconds. The time of 0.2 seconds to reach crack initiation was based on an analysis of IPIRG Task 1.0 dynamic pipe fracture experiments which involved cyclic loading; that analysis determined an "average equivalent time to crack initiation" of approximately 0.2 seconds in the various pipe tests.

J values from Battelle tests were calculated using procedures specified in ASTM 1152-87, i.e., J was separated into elastic and plastic components. As was noted above, the value of J at crack initiation was determined from d-c EP data rather than from extrapolation of J- Δa data to a blunting line or from an offset procedure.

2.3.1 Fracture Toughness of Pipe DP2-F30, ASTM A106 Grade B Carbon Steel

Dimensions of compact specimens, C(T), machined from Pipe DP2-F30 at MEA and at Battelle are summarized in Table A-1 of Appendix A. Note that the MEA specimens were appreciably thinner than the Battelle specimens.

It should be noted at the outset that several differences were observed between the quasi-static tests conducted at MEA and the quasi-static tests conducted at Battelle. In the MEA tests, which employed the unloading compliance method to monitor crack extension, maximum loads tended to be significantly lower than in Battelle tests, even after allowing for the different thicknesses and crack lengths. Furthermore, specimens tested at MEA exhibited only stable tearing, whereas those tested at Battelle showed distinct crack instabilities.

A graph of J_M versus crack extension for the two side-grooved specimens tested at MEA is shown in Figure 2.6. A blunting line was constructed through the data points near the origin (the solid line in Figure 2.6) and exclusion lines were drawn parallel with the blunting line (the two dashed lines in Figure 2.6). A straight line was fitted to the data points between the exclusion lines and extrapolated back to the blunting line. The slope of that straight line gave the value of dJ/da and the intersection of that line with the blunting line gave the value of J_i . Table 2.3 provides a summary of J_i and dJ/da values obtained from the MEA tests.

As was noted above, both quasi-static and dynamic C(T) tests conducted at Battelle exhibited crack instabilities interspersed with periods of stable tearing. Examples of data obtained in dynamic tests at 288 C (550 F) are given in Figures 2.7, 2.8, and 2.9 for Specimen F30-109. Figure 2.7 shows load, load-line displacement (LLD), ram displacement, and direct-current electric potential (U) as functions of time. A graph of load versus LLD is shown in Figure 2.8, and of electric potential versus LLD in Figure 2.9. Examination of the three graphs indicates that crack initiation probably occurred at the point indicated by a small load drop in Figures 2.7 and 2.8. That point is shown also on the electric potential versus LLD curve in Figure 2.9 and represents the earliest point on that curve that could be considered as a departure from linearity, usually taken as evidence of the onset of crack extension. Notice from Figure 2.7 that the time to achieve crack initiation was approximately 0.24 seconds, a time that was reasonably close to the desired time of 0.2 seconds.

It appears from the data in Figures 2.7 through 2.9 that the crack exhibited several periods of rapid, unstable growth—a short period right after maximum load and a longer period shortly thereafter. The strongest evidence for this assumption is the appearance of the electrical noise on the LLD signal in Figures 2.8 and 2.9; only a few cycles of noise are evident during much of the falling-load (Figure 2.8) and rising-electric-potential (Figure 2.9) part of the test, indicating that the crack was growing at an unusually high rate. The velocity of the ductile crack during the large apparent instability was estimated, from the data in Figures 2.7, 2.8, and 2.9, to be approximately 200 mm/s (8 in/s). The velocity of the ductile crack during the period of stable growth immediately following the instability was estimated to be closer to 55 mm/s (2 in/s).

The quasi-static tests conducted at Battelle at 288 C (550 F) likewise showed distinct crack instabilities. Figure 2.10 shows that these instabilities occurred for both monotonic-displacement tests and tests that employed periodic partial unloadings, similar to those used in MEA's tests. Such

Figure 2.6. J versus crack extension for Pipe DP2-F30 (A106 Grade B carbon steel) compact specimens tested quasi-statically at 288 C (550 F) at MEA

11.2-8/89-F2.6

Table 2.3. Summary of J_i and dJ/da values for Pipe DP2-F30 obtained from compact specimens tested at 288 C (550 F) at Materials Engineering Associates

(L-C orientation; 20-percent side grooves)

Specimen Identification No.	Rate	J at Initiation		$dJ/da^{(a)}$	
		kJ/m^2	$in-lb/in^2$	MJ/m^3	$in-lb/in^3$
ZP15-5LC	Quasi-static	103	590	89	12,900
ZP15-6LC	Quasi-static	97	555	92	13,300
	Avg.	100	573	91	13,100

(a) Determined for crack extension between 0.15 and 1.5 mm (0.006 and 0.06-inch) from J_D -R curve.

Figure 2.7. Transient recorder trace for dynamic C(T) test of A106 Grade B carbon steel specimen F30-109 at 288 C (550 F)
I1.2-8/89-F2.7

Figure 2.8. Load-displacement curve for dynamic C(T) test of A106 Grade B carbon steel specimen F30-109 at 288 C (550 F)
I1.2-8/89-F2.8

Figure 2.9. Electric potential versus displacement curve for dynamic C(T) test of A106 Grade B carbon steel specimen F30-109 at 288 C (550 F)
 II.2-8/89-F2.9

Figure 2.10. Load-displacement curves for quasi-static tests of compact specimens from Pipe DP2-F30 (A106 Grade B carbon steel), illustrating crack instabilities with and without partial unloadings
 II.2-8/89-F2.10

instabilities were not observed in the quasi-static tests conducted at MEA, for reasons that are not clearly evident at this time. There is some thought that the use of clip-gage control in the tests at MEA was better able to suppress cracking instability than was crosshead control used in the Battelle tests. Also, the fact that the MEA specimens were much thinner than the Battelle specimens may have played a role in the differences in behavior. In any event, inasmuch as all the Battelle tests, both quasi-static and dynamic, exhibited significant crack instabilities, graphs of J versus crack extension for the Battelle tests were terminated at the point in the test at which the first major crack instability occurred because there is no agreed upon method for calculating a J - R curve for unstable crack growth.

Figure 2.11 is a graph of load-displacement data for four quasi-static tests and four dynamic tests conducted at Battelle on 0.5T compact specimens at 288 C (550 F). Figure 2.12 shows the corresponding J - R curves for these eight specimens up to the point of the first major crack instability. Figure 2.11 does not include data for periodic unloadings or crack instabilities. Values of J_i and dJ/da are summarized in Table 2.4.

Several observations from Figures 2.11 and 2.12 and Table 2.4 are noteworthy:

- (1) In specimens that were subjected to monotonically increasing displacement, the quasi-static and dynamic load-displacement curves (Figure 2.11) were nearly identical. The main exception to that observation is that the quasi-static tests produced slightly higher maximum loads than did the dynamic tests. The observed similarity between quasi-static and dynamic tests seen in Figure 2.11 differs from the behavior displayed by other ferritic steels tested in the IPIRG program (Ref. 2.6). In a larger diameter A106B pipe (DP2-F29), the load-displacement curve for a dynamic test was significantly lower than for a quasi-static test. Conversely, the opposite was true for a submerged-arc weld in that pipe (DP2-F29W).
- (2) On average, the quasi-static J - R curves were slightly higher than the dynamic curves, and the slopes (i.e., tearing modulus) for the two displacement rates were comparable (see Figure 2.12 and Table 2.4).
- (3) Specimens that underwent periodic partial unloadings exhibited significantly lower maximum loads and greater displacements at maximum loads (see Figure 2.11) than did their monotonically loaded counterparts. A similar observation was made when comparing MEA and Battelle tests. However, the effect of periodic partial unloadings on J_i and dJ/da appeared to be small (see Table 2.4 and Figure 2.12).

The first two observations suggest that in the C(T) tests, changing the displacement rate by a factor of about 3,000 has not caused a large change in the overall resistance to ductile crack extension in this steel.

Figure 2.11. Load-displacement data for compact specimens from Pipe DP2-F30 (A106 Grade B carbon steel), tested at 288 C (550 F)
 11.2-8/89-F2.11

Figure 2.12. J-resistance curves for compact specimens, up to the point of the first large crack instability, from Pipe DP2-F30 (A106 Grade B carbon steel) tested at 288 C (550 F)
 11.2-8/89-F2.12

Table 2.4. Summary of J_i and dJ/da values for Pipe DP2-F30 obtained from compact specimens tested at 288 C (550 F) at Battelle

(L-C orientation; 23-percent side grooves)

Specimen Identification No.	Rate	J at Initiation		$dJ/da^{(a)}$	
		kJ/m^2	$in-lb/in^2$	MJ/m^3	$in-lb/in^3$
F30-112	Quasi-static	69	395	98	14,200
F30-114	Quasi-static	74	425	92	13,300
	Avg.	72	410	95	13,750
F30-113 ^(b)	Quasi-static	70	400	139	20,200
F30-115 ^(b)	Quasi-static	67	385	105	15,200
	Avg.	69	393	122	17,700
F30-108	Dynamic	61	350	157	22,800
F30-109	Dynamic	75	430	178	25,800
F30-110	Dynamic	50	285	94	13,600
F30-111	Dynamic	49	280	79	11,500
	Avg.	59	336	127	18,400

- (a) Determined for crack extension between 0.15 and 1.5 mm (0.006 and 0.06-inch) using J_D -R curve.
- (b) Specimens were partially unloaded several times prior to maximum load to simulate MEA unloading-compliance test procedure.

It should be noted that J_i values from quasi-static tests at Battelle (Table 2.4) are somewhat less than J_i values from quasi-static tests at MEA (Table 2.3). The reasons for this difference are not known with certainty, but may reflect material variability, differences in specimen thickness, or the different methods used for determining the point of crack initiation. The Battelle method of determining crack initiation, which is based on departure from linearity of the d-c EP versus displacement curve, tends to give a somewhat earlier indication of crack initiation than does extrapolation of the J - Δa curve to a blunting line.

To summarize the results of the tests reported here for Pipe DP2-F30, increasing the rate of displacement at 288 C (550 F) produced the following effects:

- Neither the load-displacement curve nor the J-R curve from 0.5T C(T) tests were influenced strongly by a factor of 3,000 increase in displacement rate. Although, on average, J_i values were slightly lower at the higher displacement rate, tearing modulus values were comparable.

- Crack instabilities observed in monotonic quasi-static tests at Battelle (but not in quasi-static unloading compliance tests at MEA) were also observed in the Battelle dynamic monotonic tests.

2.3.2 Fracture Toughness of Pipe DP2-A23, Type 304 Stainless Steel

Dimensions of the 0.5T C(T) specimens machined from Pipe DP2-A23 at MEA and at Battelle are summarized in Table A-2 of Appendix A. Two quasi-static tests at 288 C (550 F) were performed at MEA and one was performed at Battelle. The MEA tests were conducted in conjunction with the NRC's Degraded Piping Program. In MEA's original analysis of their test data, they constructed a blunting line from the formula recommended in ASTM E813-81:

$$J = 2\sigma_f \Delta a, \quad (2.1)$$

where σ_f is the average of the yield and ultimate tensile strengths and Δa is the crack extension. That blunting line provided a poor match for the experimentally determined blunting line and yielded J_i values that were unreasonably high. Battelle reanalyzed the data using a blunting line constructed from the relation:

$$J = 4\sigma_f \Delta a. \quad (2.2)$$

Equation 2.2 has been suggested by several investigators (Ref. 2.7) as appropriate for highly ductile metals such as austenitic stainless steels. That equation was found to provide good agreement between the calculated and experimentally determined blunting line and to bring the J_i value close to the value observed in the Battelle quasi-static test. Figure 2.13 shows the J-R curves from the MEA tests in which Eq. 2.2 was used to calculate the blunting line. A summary of J_i and dJ/da values for the MEA tests can be found in Table 2.5. As was noted for MEA's results for DP2-F30, Battelle's reanalysis of MEA's results for DP2-A23 employed the methods of ASTM E1152-87 to calculate deformation J-resistance curves.

In the C(T) tests at Battelle, both quasi-static and dynamic, the d-c EP results gave very poor agreement between calculated and actual crack growth. The reasons for this occurrence are not known; in the vast majority of other C(T) tests conducted at Battelle on austenitic stainless steels, agreement between calculated and actual crack growth has been excellent (Ref. 2.8). Because of the poor agreement, the d-c EP data were used only for determining the crack initiation point; crack growth was estimated by the following method.

In the single quasi-static test conducted at Battelle, crack growth during the test was estimated by constructing a smooth curve between the crack initiation point and the final crack extension on a crack extension versus displacement graph. The crack initiation point was determined from d-c EP data; the final crack extension was measured directly on the specimen after testing. Results of the quasi-static test at Battelle are shown in Figure 2.14 (load and crack extension versus displacement)

Figure 2.13. J versus crack extension for Pipe DP2-A23 (Type 304 stainless steel) specimens tested quasi-statically at 288 C (550 F) at MEA
11.2-8/89-F2.13

Table 2.5. Summary of J_i and dJ/da values for Pipe DP2-A23 obtained from compact specimens tested at 288 C (550 F).

(L-C orientation; 20-percent side grooves [MEA]
or 23-percent side grooves [Battelle])

Specimen Identification No.	Rate	J at Initiation		$dJ/da^{(a)}$	
		kJ/m^2	$in-lb/in^2$	MJ/m^3	$in-lb/in^3$
ZP17-9LC ^(b)	Quasi-static	597	3,405	286	41,500
ZP17-13LC ^(b)	Quasi-static	538	3,070	315	45,700
A23-113	Quasi-static	646	3,685	242	35,100
A23-X ^(c)	Dynamic	675	3,850	285	41,300

- (a) Determined for crack extension between 0.15 and 1.5 mm (0.006 and 0.06-inch) using J_D -R curve.
 (b) Specimen was fabricated at MEA.
 (c) Specimen No. A23-X represents a composite of five nominally identical compact specimens tested dynamically to different total displacements and different crack lengths; individual specimens were No.'s A23-108, A23-109, A23-110, A23-111, and A23-112.

and in Figure 2.15 (J-R curve). Values of J_i and dJ/da are given in Table 2.5. Note that the Battelle results and the MEA results in Table 2.5 are in reasonable agreement.

In the dynamic tests, in which crack initiation occurred in approximately 0.1 second, five nominally identical specimens were tested, each to a different final displacement and final crack length. A composite graph of load-displacement-crack extension was constructed from the data obtained in the five tests; that composite graph is shown in Figure 2.14. The curve of crack extension versus displacement in Figure 2.14 was constructed from the estimated initiation point (from d-c EP data) and the final values of crack extension (measured directly on the tested specimens). The J-R curve constructed from the results in Figure 2.14 is shown in Figure 2.15. J_i and dJ/da values are shown in Table 2.5.

It is evident from Figures 2.14 and 2.15 and from Table 2.5 that dynamic tests of specimens from Pipe DP2-A23 show moderately greater fracture resistance than do quasi-static tests. This trend was anticipated and has been observed previously in tests on another austenitic stainless steel (Ref. 2.9).

2.4 Discussion of Dynamic Rate Effects on Properties

The two materials investigated in this effort, an A106 Grade B carbon steel and a Type 304 austenitic stainless steel, exhibited markedly different responses to increased displacement rates in mechanical property tests. The carbon steel, because of its susceptibility to dynamic strain aging, showed marked

Figure 2.14. Load-displacement crack-extension curves for compact specimens from Pipe DP2-A23 (Type 304 stainless steel), tested at 288 C (550 F)
11.2-8/89-F2.14

Figure 2.15. J-resistance curves for compact specimens from Pipe DP2-A23 (Type 304 stainless steel) tested at 288 C (550 F)
11.2-8/89-F2.15

effects of strain rate in tensile test results. Of special importance to nuclear piping applications, both the ultimate tensile strength and fracture elongation at 288 C (550 F) were lowered significantly by a four-to-five order of magnitude increase in the strain rate. The stainless steel, on the other hand, showed virtually no change in tensile strength or fracture elongation as strain rate was increased, but did show a significant increase in yield strength.

With respect to fracture toughness, the effects of increasing displacement rate by a factor of approximately 3,000 were relatively modest in both materials. The J-R curve was raised slightly by dynamic testing in the stainless steel and was lowered slightly in the carbon steel. Crack instabilities at 288 C (550 F), which have frequently been observed in carbon steel tests at quasi-static rates, and are assumed to be associated with susceptibility to DSA (Ref. 2.10), were observed in the carbon steel studied here at both quasi-static and dynamic displacement rates.

The relatively modest effect of strain rate on the fracture toughness of carbon steel Pipe DP2-F30 differed from that observed for larger-diameter carbon steel pipes investigated in Subtask 1.3 of the IPIRG program (Ref. 2.6). In that study, dynamic testing significantly lowered the J-R curve for Pipe DP2-F29 and significantly raised the J-R curve for a submerged-arc weld in that pipe (DP2-F29W). The reasons for the different responses to increased strain rate in different carbon steels are not presently understood, but are believed to be associated with the details of their susceptibility to DSA.

2.5 References

- 2.1 Baird, J. D., "Strain Aging of Steel—A Critical Review", Iron and Steel, pp 450-457, Sept. 1963.
- 2.2 Manjoine, M. J., "Influence of Rate of Strain and Temperature on Yield Stresses of Mild Steel", J. of Applied Mechanics, pp. A211-A218, December 1944.
- 2.3 Dieter, G. E., "Introduction to Ductility", Chapter 1 in Ductility, American Society for Metals, Metals Park, Ohio, 1968.
- 2.4 Ernst, H., "Materials Resistance and Instability Beyond J-Controlled Crack Growth", Elastic-Plastic Fracture. ASTM STP 803 Vol. 1, C. F. Shih and J. P. Gudas, Eds., American Society for Testing and Materials, pp I-191-I-213, 1983.
- 2.5 Schwalbe, K. H. and Hellman, D., "Application of the Electric Potential Method to Crack Length Measurements Using Johnson's Formula", Journal of Testing and Evaluation, Vol. 9, No. 3, pp. 218-221, 1981.
- 2.6 Marschall, C. W., Landow, M. P., and Wilkowski, G. M., "Loading Rate Effects on Strength and Fracture Toughness of Pipe Steels Used in Task 1 of the IPIRG Program", NUREG/CR-6098, BMI-2175, Oct. 1993.
- 2.7 Balladon, P. and Foucault, M., "Comparison of Potential Drop, Acoustic Emission, and Partial Unloading Methods for the Evaluation of J-R Curves of Austenitic Stainless Steels", Proc. of 2nd CSNI Workshop on Ductile Fracture Test Methods, Report No. NUREG/CP-0064, CSNI Report No. 105, Aug. 1988.
- 2.8 Marschall, C. W., Held, P. R., Landow, M. P., and Mincer, P. N., "Use of the Direct-Current Electric Potential Method to Monitor Large Amounts of Crack Growth in Highly Ductile Metals", Fracture Mechanics: Twenty-First Symposium. ASTM STP 1074, J.P. Gudas, J.A. Joyce, and E.M. Hackett, Eds., American Society for Testing and Materials, Philadelphia, pp. 581-593, 1990.
- 2.9 Kanninen, M. F., Zahoor, A., Wilkowski, G., Abou-Sayed, I., Marschall, C., Broek, D., Sampath, S., Rhee, H., and Ahmad, J., "Instability Predictions for Circumferentially Cracked Type 304 Stainless Steel Pipes Under Dynamic Loading", Battelle Final Report to Electric Power Research Institute, NP-2347, Vol. 1, April 1982.
- 2.10 C. W. Marschall, M. P. Landow, and G. M. Wilkowski, "Effect of Dynamic Strain Aging on Fracture Resistance of Carbon Steels Operating at Light Water Reactor Temperatures", ASTM STP 1074, pp 339-360, 1990.

3.0 PIPE FRACTURE EXPERIMENTS

The primary objective of this effort was to develop experimental data on circumferentially cracked pipe under predominately inertial loading. Seven of the nine experiments conducted as part of this subtask were inertially loaded experiments. The remaining two experiments were displacement-controlled experiments. The primary purpose for conducting these two displacement-controlled experiments was to assess the effect of dynamic loading rate on the fracture behavior of carbon steel surface-cracked pipes and to provide data so that Subtask 1.1 results can be compared with Subtask 1.2 results. All pipe specimens contained pressurized subcooled water at 288 C (550 F). The specimens were either an A106 Grade B or TP304 stainless steel, 6-inch nominal diameter, Schedule 120 pipe. The TP304 stainless steel pipe was a high toughness material for which fully plastic conditions should exist. As such, limit-load analyses would be appropriate for the analysis of these experiments. The A106 Grade B pipe was a low toughness material for which contained plasticity should exist. As such, elastic-plastic methods may be required for the analyses of these experiments. Both through-wall-cracked (TWC) and surface-cracked (SC) pipe experiments were conducted. The TWC length evaluated was approximately 37 percent of the pipe circumference. The surface cracks had a crack length of 50 percent of the pipe circumference and a target crack depth of approximately 66 percent of the pipe wall.

The test matrix of pipe fracture experiments is given in Table 3.1. The initial experiment (Experiment 1.1-1) was an uncracked pipe experiment. The primary purpose of this experiment was to commission the test system. Experiments 1.1-2 and 1.1-3 were conducted with a circumferential TWC. In order to conduct these tests with pressurized water an internal bladder was required to bridge the crack and prevent the water from escaping. Experiments 1.1-4 and 1.1-5 were conducted with an external SC. These were also conducted with an internal bladder to prevent depressurization when the surface crack became a through-wall crack which would complicate the analysis of the data. The presence of the bladder required that these experiments be conducted with an external SC rather than an internal SC so that the flaw could be properly instrumented. Experiments 1.1-6 and 1.1-8 were carbon steel internal SC pipe experiments. Experiment 1.1-8 was planned to be a duplicate of Experiment 1.1-6 to assess the degree of repeatability and variability between experiments. The loading conditions for each of these experiments was comprised of dead-weight bending, inertial bending, and constant internal pressure at 15.51 MPa (2,250 psi), PWR conditions.

The remaining two experiments listed in Table 3.1 were displacement-controlled experiments. One objective of the quasi-static, monotonic experiment (1.1-9) was to provide data for assessing the practice of using strain gages to infer bending moments. The moments inferred from the strain gage data for this experiment were compared with the moments inferred from the load-cell data. The results from this experiment were also compared with the results from past quasi-static monotonic, carbon steel, surface-crack experiments from the Degraded Piping Program (Ref. 3.1) in order to appraise the consistency in results from one load frame to another. The purpose for the dynamic monotonic experiment (1.1-7) was to develop data for measuring the effects of high-rate loading on the fracture behavior of carbon steel, surface-cracked, pipe experiments. A similar assessment for through-wall cracks was made in Subtask 1.2 (Refs. 3.2 and 3.3).

Table 3.1. Test matrix for Subtask 1.1

Experiment Number	Material	Flaw Geometry^(a)	Loading Condition
1.1-1	Carbon Steel	None	Inertial
1.1-2	Carbon Steel	TWC	Inertial
1.1-3	Stainless Steel	TWC	Inertial
1.1-4	Carbon Steel	XSC	Inertial
1.1-5	Stainless Steel	XSC	Inertial
1.1-6	Carbon Steel	ISC	Inertial
1.1-7	Carbon Steel	ISC	Dynamic Monotonic
1.1-8	Carbon Steel	ISC	Inertial
1.1-9	Carbon Steel	ISC	Quasi-Static, Monotonic

(a) TWC = through-wall crack; XSC = external surface crack;
ISC = internal surface crack.

3.1 Pretest Design Analysis

Prior to conducting any of the pipe tests, analyses were performed on a model of the proposed test system to size mechanical components and to provide a baseline analysis to be compared with experimental results. The analysis was performed using the ANSYS finite-element code.

3.1.1 Pretest Design Approach

A conceptual model of the test system for the Subtask 1.1 inertial experiments is shown in Figure 3.1. The model consisted of a centrally supported pipe with concentrated masses at each end. The approach used to design the experiments was based on four fundamental premises:

1. The objective of the analysis was to size components, not necessarily to predict detailed behavior of the piping system during an actual test.
2. In light of Item 1, analyses were only conducted to the point where maximum load was reached in the test section.

**Figure 3.1. Conceptual model of Subtask 1.1 experiment
II.1-8/89-F3.1**

3. Only bounding cases of strength and toughness at the cracked section were considered, i.e., only strength and toughness properties and crack geometries that would produce the greatest loads or largest rotation requirements were evaluated.
4. The study was an engineering effort using reasonable assumptions.

This design philosophy provided a framework for selecting possible configurations, implementing analytical models of the configurations, selecting load cases, and providing rationale for rating the merits of competing design alternatives.

The model used in the finite element analyses was a four-node, four-element model and, because of symmetry, only half of the structure was modeled. The pipe dimensions and material properties used for the model are listed in Table 3.2. Also, only two pipe elements were used because the system was assumed to behave largely as a single degree-of-freedom mass and spring, see Figure 3.2.

Once a general test system configuration was selected, an approach was necessary to model the response to a loading input.

The steps taken were as follows:

Figure 3.2. Subtask 1.1 finite element model
11.0-9/87-F10

**Table 3.2. IPIRG Subtask 1.1 finite element model input data
for pretest design analysis**

Pipe Dimensions	Diameter 160 mm (6.25 in) Wall Thickness 14.3 mm (0.562 in)	
Material Properties	E 186 GPa (27×10^6 psi) ν 0.3 γ_s 76.8×10^{-6} N/mm ³ (0.283 lb/in ³)	
Loading	Water In Pipe, γ_w Inertial Weight Pressure Temperature Displacement	8.21×10^{-6} N/mm ³ (0.0302 lb/in ³) 4.45 kN (1,000 lb) 15.5 MPa (2,250 psi) 288 C (550 F) $\delta = \Psi t \sin \omega t$ $\omega = 18.85$ rad/sec (3 Hz) $\Psi = 1$ inch/cm
Damping	α' β'	0.0 1.363×10^{-3}

1. Perform linear-elastic analyses to determine:
 - a. Static displacements and loads at candidate load points, and
 - b. Natural frequency and mode shapes.
2. Screen the results to find configurations which have:
 - a. Maximum resultant moment,
 - b. Minimum servohydraulic flow requirements based on estimated required stroke to grow the crack, and
 - c. Ease of fabrication.
3. Conduct linear transient analyses to:
 - a. Determine best forcing function frequency,
 - b. Establish magnitude of dynamic amplification, and
 - c. Select best specific geometry.
4. Conduct nonlinear transient analyses using the best configuration, including a model of the flaw.
5. Refine the configuration to:
 - a. Reduce load and hydraulic flow requirements, and
 - b. Minimize logistics problems such as fabrication, siting, and damage control in the event of a double-ended break.
6. Analyze results to determine servohydraulic flow requirements.

In implementing the design philosophy, several constraints were imposed. First, only 6-inch nominal diameter pipe was considered to reduce the cost of the experiments. Also, material property data and quasi-static pipe bend test data were already available from the Degraded Piping Program for analysis and comparison purposes.

The second constraint on the design was that the test system had to be reusable. Multiple experiments were to be conducted. To control the test costs, the test system had to be designed so that damage from each test was limited to the test section as much as possible.

The last constraint was cost and availability of materials. The subtask had a well defined scope of work, schedule, and budget. The test system resulting from this design study had to be compatible with all of these.

3.1.2 Pretest Design Analysis Methods

A basic structural analysis capability was required to complete the design study. In addition, several supporting analytical developments or models were required to provide the data inputs to the structural analysis or to post-process the raw results from the analyses into system specifications. The required supporting capabilities included a model to represent system structural damping, a model of the behavior of a crack in a pipe, a means of implementing the behavior of a crack in an analysis, and servohydraulic flow analysis to size the components used to excite the piping. Because the assumptions and limitations of both the basic structural analysis tool and the supporting analytical models impact the conservatism of the designs, it is appropriate to discuss the basis of the analytical methods used.

3.1.2.1 *Structural Analysis*

The requirements for the structural analysis included linear and nonlinear analysis for both static and time history loadings and the capability to solve for frequencies and mode shapes. A capability to represent pipe displacements, internal pressure, and dead-weight loads was also required. A finite element analysis was used because it incorporates all of these features. In this case, the ANSYS computer code, a commercial program available from Swanson Analysis Systems, was used.

The system models developed with the ANSYS program used typical beam-type pipe elements to model the piping. Lumped mass elements were used to simulate inertial masses. Both of these elements are standard in most finite element program libraries.

3.1.2.2 *Structural Damping*

During the cyclic excitation of a piping system, energy will be dissipated by friction in the pipe support structures and by the pipe material itself. This dissipated energy will manifest itself as a larger applied force requirement to obtain a prescribed dynamic displacement. To model this, the analysis must include damping. Although there are several models of structural damping, classical Rayleigh damping was used in this study because it is easily implemented in a finite element analysis.

3.1.2.3 *Nonlinear Modeling of Cracked Pipe Sections*

In order to analyze the behavior of a piping system containing cracks, a simple nonlinear representation of the crack was developed. The model, a one-dimensional (i.e., moment versus rotation) nonlinear stiffness, accounts for local plastic deformation due to the crack and stable crack growth occurring during loading.

The starting point for the crack section modeling used in this study was the quasi-static load-displacement test data from the Degraded Piping Program for circumferential cracked pipe

under four-point bending. In calculating the stiffness of a cracked section, the material and crack section geometry (i.e., pipe diameter, thickness, crack length, and crack depth) for the proposed dynamic analysis must be reasonably similar to conditions for a quasi-static bend test for which data are available. In addition to having quasi-static load-displacement or moment-rotation data for a given crack geometry, several assumptions must be made to generate the crack stiffness:

- For low-cycle high-amplitude loading, the cyclic load effect on circumferential crack growth is assumed to be small; growth is dominated by ductile tearing under increasing load amplitude.
- High strain rate effects on crack growth and on the constitutive relationship are ignored, even though they may tend to influence crack growth and plastic flow to some extent for some materials.
- Only a separation mode (Mode I) of crack propagation under bending is assumed. This implies that the crack section is rigid in all degrees of freedom except the rotation corresponding to crack opening. The axial force due to internal pipe pressure is assumed to be constant and is accounted for by reducing the moment-rotation curve.
- The plastic deformations due to the rotations of the pipe are confined to the crack section.

With these assumptions and the quasi-static test data, a procedure for generating a bending moment, M , versus rotation, ϕ can be developed for an inertially loaded, circumferentially-cracked pipe.

3.1.2.4 Modifications to the Nonlinear Model of Cracked Pipe Sections

The moment-rotation, M - ϕ , curve as described above provides the basic stiffness for a cracked section. At times, however, it was desirable to make modifications to the basic curve to account for a difference in internal pressure on a cracked pipe. In these cases, simple adjustments can be made. (Note: 2ϕ is the total rotation of one end of the pipe relative to the other end of the pipe.)

The method used to incorporate axial tension effects on a crack, when the tension is due to internal pressure in the pipe, is to modify the nominal M - ϕ curve for the non-pressure case by uniformly reducing all of the moments by a constant factor. This factor is determined from simple relationships between the nominal conditions and the desired conditions. Because our model of a crack is only one dimensional, i.e., it does not respond to the axial load caused by pressure, the effect of pressure on crack separation can only be modeled by a reduction in the moment-carrying capacity of the crack by lowering the M - ϕ curve.

The data used for generating the $M-\phi$ curves were from the Degraded Piping Program Experiments. These had been conducted without internal pressure, (Ref. 3.1). Therefore, the $M-\phi$ curves for all the pressurized experiments needed correcting.

3.1.2.5 Adjustments of Moment Due to Internal Pressure

Hoop stress due to pressure has little effect on the behavior of the crack. Tensile stress, however, caused by pressure will contribute to the circumferential growth of a crack. According to Net-Section-Collapse analysis, the angle to the bending neutral axis, β , (Figure 3.3) can be found from equilibrium considerations as

$$\beta = 1/2 (\pi - \theta d/t) - \frac{\pi p (R_m - t/2)^2}{4 R_m t \sigma_f} \quad (3.1)$$

Figure 3.3. Nomenclature and loading for the application of the Net-Section-Collapse criterion to a pipe with a circumferential surface crack subjected to combined pressure and bending
II.0-9/87-F3R

where p is the pressure, σ_f is the flow stress, R_m is the mean pipe radius, and the other terms are as described in Figure 3.3. The above equation is perfectly general, being applicable to pressurized or unpressurized pipes, surface cracks or through-wall cracks depending upon p and d/t . Accordingly, the limit moment is

$$M = 2\sigma_f R_m^2 t (2\sin\beta - [d/t]\sin\theta). \quad (3.2)$$

Denoting a factor of $f_p = M(p)/M(p=0)$, where $M(p)$ and $M(p=0)$ are the moments using Equation 3.2 for pressurized and unpressurized pipe, respectively, the bending moments observed in the previous quasi-static experiments can be adjusted for pressure by multiplying the nominal M for the unpressurized case by the factor f_p . This will give an approximate correction sufficient for engineering calculations.

3.1.2.6 *Summary of Pretest Design Calculation Procedures Used*

Table 3.3 summarizes the crack section moment-rotation calculation procedures. When applying these procedures, some engineering allowance was made for spurious data points—negative rotations, rotations that do not smoothly increase with moment at low moments, etc. These data were eliminated.

The procedure described above is contingent upon having an experimental load-displacement data record for the material and crack geometry of interest. Lacking this, an alternative is available; analytically predicting the load-displacement behavior using a J-estimation scheme along with tensile data and J-resistance characteristics determined from a standard laboratory specimen test. The computer program NRCPIPE, developed in the Degraded Piping Program, can be used to do such a calculation for both TWC and SC pipe. However, actual test data were preferred because fewer assumptions were necessary.

Table 3.3. Summary of nonlinear crack stiffness calculations

Given: Load-displacement data points, (F_i, δ_i) , from a static four-point bend test conducted at temperature T_0 .

Result: Moment-rotation data points, (M_i, ϕ_i) at temperature T and pressure p .

Step 1: Calculate pressure correction factor based on Net-Section-Collapse Analysis predicted moments at zero pressure, and test pressure, p .

$$f_p = \frac{M(p, T)}{M(p = 0, T)}$$

Step 2: Calculate crack section moment.

$$M_i = f_p \left[F_i A' + \frac{W(S-2l)}{4} \right]$$

Step 3: Calculate crack section rotation in the linear load-displacement region based on CMO.

$$\phi_i = \sin^{-1} \left[\frac{CMO}{2(D-tx)} \right]$$

Step 4: Calculate crack section rotation assuming a value for the machine compliance, C .

$$\phi_i = \tan^{-1} \left[\frac{1}{A'} \left[\delta_i - \frac{F_i}{24EI} (L-l)^2 (L+2l) - C(F_i + W) \right] \right]$$

Step 5: Minimize the differences between the CMO-based rotation and the rotation calculated in Step 4 by adjusting C through an iteration process.

3.1.3 Finite Element Implementation of Moment-Rotation Response

The approach used to model the moment-rotation response of the flaw in the pipe is shown in Figure 3.4. Using load-displacement data generated in the Degraded Piping Program for a quasi-static test of a pipe with a flaw of the same size and made of the same material as used in the IPIRG tests, a moment-rotation curve for the crack is established as described above. This moment-rotation curve considers the crack in complete isolation, with all compliance due to the test machine and main body of the pipe removed. The resulting moment-rotation response of the flaw then becomes the defined response between two adjacent pipe elements at the crack location.

Implementation of a crack's moment-rotation response in a finite element analysis basically requires definition of a nonlinear spring. Within ANSYS, a convenient means of achieving this is with spring-slider elements, one of which is shown in Figure 3.5. The spring-slider element is a two node, two degree-of-freedom element with a linear spring and a friction slider in series. At low loads, the slider is locked and the two nodes behave as if they have a linear spring between them. When the spring load gets to $\pm F_{\text{slide}}$, the force between nodes I and J remains constant. If the spring-slider reaches the limiting force and then is unloaded, a permanent displacement will remain when the load is brought back to zero. Putting two or more spring-slider elements in parallel with different stiffnesses and F_{slide} values allows one to model a multilinear load-deflection curve. Figure 3.6 illustrates the modeling of a 3 segment curve. Note, the nonlinear spring element provided within ANSYS would have been easier to implement, but it did not allow for unloading and reloading through zero load in the manner desired.

Under monotonically increasing load, a multilinear spring-slider representation of a crack follows the curve as shown in Figure 3.6. With load reversals, as in the Subtask 1.1 experiments, compressive yield and hysteresis effects become important. Referring to Figure 3.7, at the point of unloading, the spring-slider model unloads along a line parallel to the slope of the initial segment of the defined moment rotation curve. Unloading continues along this line until the load reaches a value of $2F_{T1}$ below the load at which unloading took place (the term F_{T1} is defined in Figure 3.6). At this point the system unloads along a line parallel to the slope of the second segment, to a load level that is $2(F_{T2} - F_{T1})$ below the slope change (yield). Subsequent yielding and load levels continue with the same pattern.

The behavior of the spring-slider elements described above is a model of the Baushinger effect, i.e., kinematic hardening in a plasticity analysis. Given a moment-rotation curve for a particular crack, a multilinear spring-slider model of the crack response can be generated as described above (in this case, moment is the force and rotation in radians is the displacement). This multilinear spring-slider model is then attached to a pair of coincident nodes between any two adjacent pipe elements to define the coupling between the in-plane bending degree-of-freedom of the pipe elements. To couple the remaining degrees-of-freedom (one remaining bending, the torsional, two shear, and the axial degrees-of-freedom) constraint equations are

- (1) Take static cracked pipe data from DP 311

- (2) Reduce P- δ into moment-rotation diagram only for the cracked pipe section

- (3) Incorporate incremental stiffness into the FEM code

$$[K](\Delta q) + [C](\Delta \dot{q}) + [M](\Delta \ddot{q}) = (\Delta F)$$

$$[K] = \begin{bmatrix} F_x & F_y & F_z & M_x & M_y & M_z \\ u_x & u_y & u_z & \phi_x & \phi_y & \phi_z \\ \infty & & & -0- & & \\ & \infty & & & & \\ & & \infty & & & \\ & & & \infty & & \\ & & & & \infty & \\ -0- & & & & & \infty \\ & & & & & \frac{\Delta M}{\Delta \theta} \end{bmatrix}$$

: positive definite

Figure 3.4. Nonlinear cracked-pipe section modeling
II.0-9/87-F4

Figure 3.5. Basic spring-slider element
II.0-9/87-F5

$$\begin{aligned}
 K_1 + K_2 + K_3 &= K_{T1} & F_1 + (K_2 + K_3)D_{T1} &= F_{T1} \\
 K_2 + K_3 &= K_{T2} & F_1 + F_2 + K_3 D_{T2} &= F_{T2} \\
 K_3 &= K_{T3} & F_1 + F_2 + F_3 &= F_{T3}
 \end{aligned}$$

Figure 3.6. Modeling of a multilinear load-displacement curve
 II.0-9/87-F6

Figure 3.7. Spring-slider model of a crack under reversed loading
 II.0-9/87-F7

used which specify that displacement in these degrees-of-freedom must be identical for the two pipe elements on either side of the crack. Completing the definition of the crack element, crack closure is modeled as a constraint such that no rotation in the negative direction is allowed, i.e., the crack becomes rigid when it closes, transmitting all moments continuously across the crack.

The crack model as described above was used in all of the design analyses that included the effects of the crack. The model is quite simple and as long as the loading does not take excessively large steps, closed hysteresis loops are traced on unloading as shown in Figure 3.8. Maximum moment in the crack section occurs when all three spring-sliders are at the limiting slide force and the crack rotation becomes large. This model is consistent with the design philosophy in that it is only valid up to maximum load.

Figure 3.8. Typical moment-rotation behavior of multilinear spring-slider crack model

I1.0-9/87-F8

3.1.3.1 Servohydraulic Flow Analysis

Given a piping configuration and an input forcing function that will cause the crack to reach maximum moment, i.e., all three spring-sliders are at their limiting load, analysis must be performed to size the servohydraulic components. Specifically, the hydraulic actuator size in terms of area and stroke must be determined, the rated flow size for the servovalves must be established, and the size of accumulators and pumps must be set.

The data inputs to the flow analysis were the applied displacement, velocity of the actuator and applied force as a function of time, hydraulic supply pressure, assumed accumulator volume and precharge pressure, and assumed line losses.

The servovalve was modeled as an orifice plumbed in a four-way mode (push-pull). The accumulators were assumed to be charged such that the gas was compressed isothermally and all oil during a test was supplied by the accumulators. The last point considered in performing the servovalve flow analysis was that servovalves were sized at a fixed pressure drop across the inlet and outlet.

3.1.4 Dynamic Loading Function

To perform the pretest design analyses of the experiments, dynamic loading functions had to be selected. The criteria used for selection of the loading functions were:

- the function must be relatively simple, to avoid introducing an excessively large number of unknowns into the experiments,
- the behavior of cracked test sections should be dominated by ductile tearing as opposed to fatigue crack growth,
- for the inertially loaded experiments, the functions must be able to excite the test systems near resonant modes to reduce the energy input needed to reach maximum moment, and
- for the dynamic monotonic, carbon steel, surface-cracked pipe experiment (1.1-7), the strain rate at the crack should be similar to that for the Subtask 1.2 (Refs. 3.2 and 3.3) dynamic monotonic, carbon steel, through-wall-cracked-pipe experiment.

Although a realistic seismic forcing function has some intrinsic appeal for the inertial experiments, because this is what piping in a plant would experience, it certainly does not meet the first loading function criterion and probably would fail the second and third also. In addition, actual seismic signatures are unique and vary widely from one location to another, making the selection of a particular seismic forcing function rather arbitrary. Instead, a decision was made, for the inertially loaded experiments, to use modified sinusoidal forcing functions at about 70 to 90 percent of the first natural frequency of the test system, with a design target of achieving maximum moment within 5 to 20 cycles. To minimize servo-

hydraulic control and structural transient response problems, ramping functions were used to modify the basic sinusoidal forcing functions.

In practice, the forcing functions that were used in the design analyses and the forcing functions that were used in the experiments were not identical. Because this study was an engineering design effort, relatively severe forcing function amplitudes were used in the design analysis to limit the dynamic, nonlinear transient structural analysis simulation times. This tended to cause maximum moment in the cracked sections to occur in less than 10 cycles, and it allowed a larger number of design configurations to be explored in a given time. Short simulation times tended to minimize the build-up of resonant dynamic response; hence, the displacements, velocities, and forcing function inputs needed to grow the initial cracks were greater than observed in the actual experiments.

3.1.5 Results of Pretest Design Analyses

The results of the design analyses for the inertial experiments consisted of moment in the test section, applied force at the load points, displacements of the inertial masses as a function of time, and the servohydraulic flow requirements.

The pretest design analyses for the quasi-static and dynamic monotonic experiments were not as rigorous as those for the inertial experiments. The analysis consisted simply of assessing whether the existing actuators had the stroke and force capacity required for the experiments. Past experimental data and a number of fracture criteria (i.e., Net-Section-Collapse) were assessed to see if the existing actuators were sufficient for these monotonic experiments. In addition, for the dynamic monotonic experiment, it was necessary to choose a loading rate for the experiment. The loading rate was chosen so that the strain rate would be similar to that of the Subtask 1.2 (Refs. 3.2 and 3.3) dynamic monotonic, carbon steel, through-wall-cracked-pipe experiment. Once the loading rate was decided upon, it was necessary to ensure that the servovalve rated flow capacity was sufficient.

3.1.5.1 Inertially Loaded Carbon Steel Surface Crack Pretest Design Analysis

This experiment was analyzed since it was believed to require the greatest load of any material and crack configuration in the original test matrix. The input data for generation of the moment-rotation curve came from Degraded Pipe Program Experiment 4112-6. Based on the procedures discussed above, the load-displacement data for Experiment 4112-6 were converted to the moment-rotation curve shown in Figure 3.9. A correction for a machine compliance of 10.3 mm/MN (1.80×10^{-6} in/lb) and a correction for internal pressure were made to produce Figure 3.9. Also shown in Figure 3.9 is the three-segment curve used to discretize the moment-rotation curve for the finite element analysis.

Figures 3.10, 3.11, and 3.12 show the moment in the test section, the applied actuator force, and the inertial mass displacement as a function of time for this analysis. The maximum moment at the crack section occurs at $t = 2.920$ seconds (10 cycles) for the forcing function shown in Figure 3.11. Actuator force varies from +31.0 kN (+6,960 pounds) to -16.7 kN

Figure 3.9. IPIRG Subtask 1.1 cracked-pipe-section moment-rotation curve for carbon steel surface-cracked-pipe experiment
I1.0-9/87-F12R

Figure 3.10. Subtask 1.1 test section moment for the carbon steel surface-cracked-pipe analysis
I1.0-9/87-F13R

Figure 3.11. Subtask 1.1 actuator force for the carbon steel surface-cracked-pipe analysis

I1.0-9/87-F14R

Figure 3.12. Subtask 1.1 inertial mass displacement for the carbon steel surface-cracked-pipe analysis

I1.0-9/87-F15

(-3,760 pounds) while actuator stroke (not shown) went from +70.0 mm (2.75 inches) to -74.0 mm (-2.92 inches). Plotting moment versus rotation, closed hysteresis loops are traced with the same shape as the input moment-rotation curve, see Figure 3.13. As expected, the moment-rotation plot exhibits crack closure, reversed plasticity and very large rotations at maximum moment.

Completing the analysis of the Subtask 1.1 carbon-steel surface-crack test, a servohydraulic analysis was performed. For this analysis, it was assumed that all of the flow would be supplied by a pump with no line losses at 20.7 MPa (3,000 psi). Sizing the actuator to meet the maximum force, 31.0 kN (6,960 pounds), and maximum velocity requirement, 1.36 m/sec (53.4 in/sec), results in a minimum required actuator size of 1,520 mm² (2.35 in²). A rated servovalve size of 101 liters per minute (27 gpm) at a rated pressure drop of 6.9 MPa (1,000 psi) is needed to meet the force and velocity requirements for this test. The total volume of oil needed to conduct the test under these conditions is 2.0 liters (0.5 gallons).

3.1.5.2 Inertially Loaded Stainless Steel Through-Wall Crack Pretest Design Analysis

This experiment was also analyzed since it was believed to require the greatest displacement of all the experiments in the original test matrix. The load-displacement data for the moment-rotation curve for the stainless steel through-wall-cracked pipe experiment came from Experiment 4131-5 of the Degraded Piping Program. Figure 3.14 shows the pressure corrected moment-rotation curve and the approximation used in the finite element analysis.

Using the forcing function $\delta = \Psi t \sin \omega t$, where $\omega = 18.85$ radians/second (3 Hz) and Ψ is a constant equal to 25.4 mm/sec (1.0 inch/sec), the analysis was conducted until maximum moment was detected in the test section. Figures 3.15, 3.16, 3.17, and 3.18 show the moment-time, applied force-time, inertial mass displacement-time, and moment-rotation plots, respectively. Maximum moment occurs at $t = 1.325$ seconds (5 cycles), with forces reaching +12.8 kN (+2,870 pounds) tension and -3.1 kN (-700 pounds) compression. Actuator stroke requirements are +27.6 mm (+1.09 inches) and -31.8 mm (-1.25 inches). A servohydraulic analysis indicates that a 38-liter-per-minute (10 gpm) servovalve rated at 6.9 MPa (1,000 psi) pressure drop would be adequate. This analysis assumes a constant supply pressure of 20.7 MPa (3,000 psi), i.e., no accumulators and no line losses, and an actuator area of 760 mm² (1.17 in²). This is the minimum actuator area needed to meet the maximum force 12.8 kN (2,870 pounds) and maximum velocity 0.62 m/sec (24.5 in/sec) requirements.

3.1.5.3 Summary of Pretest Design Analyses

The design of the Subtask 1.1 test system was governed by the carbon steel surface-cracked-pipe test. Table 3.4 summarizes the minimum system requirements for Subtask 1.1. It should be noted that the servovalve and actuator force requirements apply to only half of the structure, i.e., two identical actuators and servovalves were used. To account for possible variations in pipe properties, crack response, load system flexure, and line losses, the actuators and servovalves used were somewhat larger than those suggested by the design analyses.

Figure 3.13. Subtask 1.1 output moment-rotation curve for the carbon steel surface-cracked-pipe analysis
I1.0-9/87-F16R

Figure 3.14. IPIRG Subtask 1.1 cracked-pipe-section moment-rotation curve for stainless steel through-wall-cracked-pipe experiment
I1.0-9/87-F17R

Figure 3.15. Subtask 1.1 test section moment for the stainless steel through-wall-cracked-pipe analysis
 I1.0-9/87-F18R

Figure 3.16. Subtask 1.1 actuator force for the stainless steel through-wall-cracked-pipe analysis
 I1.0-9/87-F19R

Figure 3.17. Subtask 1.1 inertial mass displacement for the stainless steel through-wall-cracked-pipe analysis
I1.0-9/87-F20

Figure 3.18. Subtask 1.1 output moment-rotation curve for the stainless steel through-wall-cracked-pipe analysis
I1.0-9/87-F21

**Table 3.4. IPIRG Subtask 1.1 minimum system requirements
based on pretest design analysis**

Actuator Area	1,520 mm ² (2.35 in ²)
Actuator Stroke	+ 70.0 mm (2.75 in) - 74.0 mm (2.92 in)
Servo valve Rated Flow*	101 liters/min (27 gpm)
Required Oil Volume	2.0 liters (0.5 gallons)
Ground Clearance at Inertial Mass	256 mm (10.1 in)

* At 6.9 MPa (1,000 psi) pressure drop, 20.7 MPa (3,000 psi) supply pressure, no line losses.

3.2 Description of Experimental Procedures

The facility, specimen preparation, instrumentation, data acquisition, test procedures, and data reduction procedures are described in the following sections.

3.2.1 Test Facility

Figure 3.19 shows an aerial view of the test facility located at Battelle's West Jefferson, Ohio, site. This is a sparsely populated, remote location which is commonly used for tests involving large, rapid, energy release rates. The test frame, Figure 3.20, was constructed using two 67 kN (15 kip) hydraulic actuators mounted parallel to each other. The hydraulic actuators were attached at floor level to a steel subframe placed into a hole measuring two meters on a side which was then filled with 8 cubic meters (10.5 cubic yards) of concrete. The weight of the foundation (180 kN [40.5 kip]) exceeded the force capability of the actuators, thus reducing the likelihood of the foundation being lifted by inertial forces during a test. The actuator rods were located on 914 mm (36 inch) centers and were operated in parallel by a single servocontroller driving two 227 lpm (60 gpm) Moog servovalves, each mounted to an actuator. The actuator size and hydraulic flow rate were approximately twice the size suggested by the pretest design analysis in order to compensate for possible errors in the design assumptions. Engineering prudence required a reasonable operating margin be constructed into the test system.

Figure 3.19. Aerial photo of West Jefferson site

Figure 3.20. Sketch of test frame with dimensions of specimen
 II.1-10/89-F3.20

For the two monotonic experiments, steel framework was positioned underneath the masses such that when the actuators were pulled down the pipe section was put into four-point bending, see Figure 3.21. Roller bearings were placed on top of the steel framework underneath the masses to allow the masses to translate as the pipe was being bent.

Figure 3.21. Load frame used for the two Subtask 1.1 displacement-controlled experiments (1.1-7 and 1.1-9)
 II.1-6/91-F3.21R

The test frame was housed inside a building designed with multiple large access doors to allow the rapid dissipation of internal pressure caused by the failure of the pipe specimens being tested. A separate building, 80 m (260 ft) away, was used to control and monitor the pipe experiments.

3.2.2 Pipe Specimen Preparation and Instrumentation

The pipe test specimens were made up of a reusable portion (the moment arms and masses) and replaceable portions. The replaceable portions were short sections of Schedule 120 pipe material, either austenitic or ferritic steel according to the test matrix, which were welded to the moment arms. Specimen flaw generation and internal instrumentation or bladder installation were performed, using the following procedures, before welding to the moment arms. The materials used in these experiments were an A106 Grade B carbon steel with a Battelle identification of DP2-F30 and DP2-F31, and an SA-376 TP304 stainless steel with a Battelle identification of DP2-A27. (Pipe A27 was from the same heat as Pipe A23 used to develop the material property data.)

After selecting 460 mm (18 inch) lengths of the appropriate test material, a flaw was initially machined into the specimen using a slitting saw to obtain the rough dimensions of the flaw.

The root of the saw cut was then sharpened using electro-discharge machining (EDM) techniques for surface flaws or a narrow blade jeweller's saw for the through-wall flaws. For the ferritic pipes, the flaw was further sharpened by fatigue precracking. In the austenitic materials, the flaws were not fatigue precracked due to the relative insensitivity of this stainless steel material to notch tip acuity.

For the first four inertial experiments, a 254-mm x 254-mm x 1.5-mm (10-inch x 10-inch x 0.060-inch) thick stainless steel sheet that had been rolled to the approximate inside diameter of the specimen was placed inside the specimen to bridge the flaw. Prior to insertion, this sheet was flame sprayed with a ceramic insulating material in order to electrically isolate the sheet from the specimen. This electrical isolation allowed for better electric potential data which were used to monitor crack initiation and growth. A high temperature Viton® bladder was then installed inside the specimen. Figure 3.22 shows an end view of a stainless steel specimen with the Viton® bladder installed. The ends of the bladder were sealed to the inside diameter of the pipe specimen using a series of large steel snap rings and a liquid pre-vulcanized form of the Viton® material. For the through-wall-cracked-pipe experiments, the bladder and steel patch acted as a pressure boundary. For two of the surface-cracked pipe experiments (Experiments 1.1-4 and 1.1-5), the bladder and steel patch allowed the internal pressure to be maintained for some time after the surface crack grew through the wall. This eliminated any effects of pressure blowdown until substantial crack growth had occurred.

After the bladder was installed and the steel moment arms welded onto the specimen, the pipe saddles were installed. The saddles were specially designed for these tests to allow for rotational and axial freedom of the pipe while providing minimum transverse clearance as the load was reversed on the specimen during a test. Figure 3.23 is an engineering drawing of the pipe saddles.

The completed specimen was mounted on the test frame and was instrumented with high temperature weldable strain gages. The measured strains would be used for calculation of moment in the test section. The test frame hydraulic actuators were instrumented with short body displacement transducers known as LVDTs for measurement of load point displacement and to provide displacement feedback for the servo-controllers. Load cells were mounted between the actuators and the saddles. The load cells provided an alternative means of checking the moments calculated from the strain measurements for the two displacement-controlled experiments (1.1-7 and 1.1-9). It had been hoped that they would also provide an alternative means of calculating moments for the inertial experiments, but it did not work out. The test section was also instrumented to measure crack-mouth-opening displacement, rotation of the cracked section, crack initiation, specimen temperature, and internal pressure.

Crack-mouth-opening displacement was measured by a clip gage mounted across the flaw. The output from this instrument was conditioned through a standard Vishay strain gage amplifier.

For the initial series of cracked experiments (1.1-2 through 1.1-6), the rotation about the crack plane was measured using a specially fabricated instrument employing a rotary variable

Figure 3.22. End view of stainless steel specimen showing Viton® bladder and steel snap rings

II.1-6/91-F3.22

Figure 3.23. Engineering drawing (units in mm) of pipe saddle
 I1.1-10/89-F3.22

**Figure 3.24. Rotation instrument mounted to TWC pipe
which is being fatigue precracked**

I1.1-6/91-F3.24

differential transformer (RVDT) as the sensing element. Figure 3.24 shows the rotation instrument mounted on a specimen. This rotation device provided an additional function for these early experiments; it provided the signal to end an experiment. Rotation about the crack plane was a reliable indicator of test status and when a predetermined angle, i.e., voltage, was achieved, a comparator circuit was triggered. This circuit began sequencing a solid state potentiometer, which was in the servocontroller command circuit, softly shutting down the system. This prevented any damage that might result from a sudden shutdown. For the last three experiments (Experiments 1.1-7 through 1.1-9), the crack section rotation measurements were made using an improved torque bar arrangement with an LVDT as the sensing element, see Figure 3.25.

Crack initiation and growth were indicated by changes in the d-c electric potential in the vicinity of the flaw. The electric potential field was created by a constant electric current across the flaw. The electric potential field was measured local to the flaw. Field perturbations caused by crack initiation and growth were also measured. For the austenitic specimens, a conventional 80 ampere current source was adequate to obtain good data. However, this 80-amp current source was not sufficient for tests involving ferritic materials. The carbon steel materials exhibited a significant self generated voltage when loaded dynamically which completely obscured the small microvolt variations indicating crack initiation and growth. A more satisfactory result was obtained by increasing the current source, thereby proportionally increasing the voltage level of the crack initiation and growth data. The increased current was supplied using a single cell lead-acid (Planté) battery whose capacity was 13,900 amperes. Since the cell had a voltage level of only 2 volts, the actual current achieved for these tests was limited to approximately 4,000 amperes due to the combined cable lead and specimen resistance of about 0.5 m Ω .

Figure 3.25. LVDT-based rotation device used on Experiments 1.1-7, 1.1-8, and 1.1-9
 I1-2-6/91-F3.25

The temperature of the specimen was measured by Chromel-Alumel (type K) thermocouples attached at three positions near the flaw and at three additional positions along the specimen length.

The internal system pressure was monitored using a Shaevitz pressure transducer. For the final three surface-cracked pipe experiments (Experiments 1.1-7, 1.1-8, and 1.1-9), internal pipe pressure provided the signal to end the experiment. Once the surface crack penetrated the pipe wall, the internal pipe pressure dropped immediately. (No bladder was used in these three experiments.) Once the pressure dropped to a predetermined level, a signal was sent to the servo-controller to softly shutdown the forcing function.

Additional instrumentation included devices to measure the vertical displacement of the end masses during the inertial experiments. These were simply space frames built from aluminum tubing which were mounted on an axle at one end and attached to the mass at the other (see Figure 3.20). As the mass travelled vertically, the space frame rotated about the axle and this rotation was sensed by an RVDT.

For some of the early experiments, accelerometers were also mounted to the masses and the center pipe attachment saddles.

Once the instrumentation was complete, the pipe was wrapped with heater tapes and insulation and filled with water. The pipe was pressurized to approximately 1 MPa (150 psi) and the heating tapes were energized. Heating and pressurization continued until PWR conditions were achieved in the test specimen. After maintaining the test specimen at temperature for a minimum of an hour, data collection instrumentation were started and the test was initiated.

3.2.3 Data Acquisition

Data acquisition for these experiments consisted of three independent systems. The primary system for all except the dynamic monotonic experiment (1.1-7) was an IBM compatible personal computer running an analog-to-digital (A/D) converter board produced by Metrabyte® Corporation. The board was controlled by LabTech Notebook® software. For the inertial experiments, this system acquired 16 to 24 channels of data at a single channel sampling rate of 100 to 150 samples per second. The acquired data were stored on the PC's hard drive for later retrieval and reduction. For the dynamic monotonic experiment, the primary system was an FM tape recorder. The acquired analog signals were played back at 1/16 of the recorded tape speed through an IBM-compatible PC sampling at a rate of 200 Hz. Thus, the effective data acquisition rate for this experiment was 3,200 Hz. A backup data acquisition system for the initial series of experiments consisted of a MassComp computer which had a data acquisition rate set at 1,000 samples per channel per second. These data were used to improve the time resolution of parameters which occurred at frequencies higher than the 3.5 Hz test frequency. In addition to the two computer digital data acquisition systems, an analog tape recorder was used to backup the most critical data.

3.2.4 Data Reduction Procedures

The procedures used to reduce the data were applied primarily to the data collected by the Metrabyte® A/D card. The acquired data were stored on a disk in a format which could be loaded directly into a Lotus 123® spreadsheet. The algebraic manipulations required to obtain engineering units from the collected voltages were all performed within that spreadsheet.

The load cells were BLH® brand, shear web construction, strain gage element, low profile load cells. The cell capacities were 88.9 kN (20 kip) with a 2 mvolt/volt output. The load cells were excited at a level of 10 volt d-c and the outputs were amplified to yield 8.89 kN (2,000 lb) per volt. The data collected were converted to load by multiplying volts by 8.89 kN/v (2,000 lb/v).

The ram displacement transducers were Shaeffert brand short body LVDTs. These devices had a stroke capacity of ± 127 mm (± 5 inches) with an output of ± 10 v. The LVDT power was provided by an a-c conditioner module in an MTS 458-1 servo control unit. The conversion applied to these data was 12.7 mm (0.5 inch) per volt. These transducers provided the active feedback for the servo-controllers as well as the vertical displacement information for the load points.

For Experiments 1.1-2 through 1.1-6, the rotation of the pipe about the crack plane was measured by a Shaeffert RVDT whose output was calibrated and found to be 7.9899 degrees per volt. The 125 mm (5 inch) spacing of the rotation device mounting pads on either side of the crack was such that the measured rotation included some elastic rotation of the short length of included pipe. This additional rotation was quite small and was not subtracted from the total.

For Experiments 1.1-7 through 1.1-9, the crack section rotations were measured using the torque bar/LVDT arrangement shown in Figure 3.25. Using this arrangement, the total rotation, 2ϕ , of the cracked section is

$$2\phi = \tan^{-1} \left[\frac{\Delta}{S'} \right] \quad (3.3)$$

where

2ϕ = Total cracked section rotation (see Figure 3.25), radians

Δ = Displacement output of the LVDT, mm

S' = Span of the torque bar/LVDT arrangement (see Figure 3.25), mm.

Equation 3.3 is a valid approximation as long as the rotations are small. Such is the case for the surface-cracked pipe experiments for which this device was used.

The strain gage data were provided by high temperature weldable gages made by Eaton. These were 120 Ω single element gages conditioned by Vishay® amplifiers to give an output

of 1,000 microstrain per volt. For inertial Experiments 1.1-3 and 1.1-5, four gages were mounted inside the load points in a region of relatively uniform bending moment. These gages were used to calculate the moment of the test section. In subsequent experiments, additional gages were attached to the pipe outside the load points in a region where a bending moment gradient existed. Where possible, the strain gages inside the load points were used to calculate the cracked section bending moments for these experiments. The moments calculated from the gages outside the center span were used to validate the moments calculated from the inner gages.

The most general calculation of moment in a pipe requires knowledge of the longitudinal stress around the complete circumference of the pipe. Under elastic conditions, and assuming uniform wall thickness, the longitudinal stress distribution at a given cross section can be completely characterized by longitudinal stresses at three locations around the circumference. For the Subtask 1.1 experiments, an assumption is made that the out-of-plane bending moments are negligible. As a result, the in-plane bending moments can be calculated from a knowledge of the longitudinal stresses at the top and bottom of the pipe, see Equation 3-4.

$$M = \left[\frac{\sigma_T - \sigma_B}{2} \right] \frac{I}{c} \quad (3.4)$$

where

- M = In-plane bending moment,
- σ_T = Longitudinal stress at the top of the pipe,
- σ_B = Longitudinal stress at the bottom of the pipe,
- I/c = Section modulus for the pipe.

For the Subtask 1.1 experiments, single uniaxial high temperature weldable strain gages were mounted in the longitudinal direction of the pipe on the top and bottom surfaces, directly opposite one another in the same cross section. If one further assumes that the hoop strains at the cross section of interest are constant around the pipe circumference, then the in-plane bending moment at that cross section for plane stress conditions can be expressed in terms of the longitudinal strain values.

$$M = \frac{1}{2} \frac{E}{(1 - \nu^2)} (\epsilon_T - \epsilon_B) \frac{I}{c} \quad (3.5)$$

where

- E = Elastic modulus,
- ν = Poisson's Ratio,
- ϵ_T = Longitudinal strain at the top surface of the pipe,
- ϵ_B = Longitudinal strain at the bottom surface of the pipe.

For the Subtask 1.1 experiments, only the dynamic component of the longitudinal strains was considered in calculating the in-plane bending moments. Due to electrical drift in the strain gage circuits over the several hours it took to heat the specimen to PWR conditions, it was felt prudent to subtract the initial strain values from the strain gage experimental data sets.

To compensate for this over correction, an ANSYS calculated static moment due to the weight of the pipe, water, masses, and fixtures was added to the dynamic moment data.

The CMOD clip gage was a strain gaged spring steel displacement transducer whose output was conditioned using standard strain gage amplifiers.

The saddle and mass accelerometers were PCB® brand piezoelectric accelerometers. These accelerometers sensed dynamic acceleration only, but they were sensitive and had good accuracy at frequencies as low as 0.5 Hz. Their outputs were calibrated to be 5.657 g/v between 0.5 Hz and 2,000 Hz.

The electric potential (EP) probes were mounted near the crack tips and varied in number depending on the flaw geometry. The output of the probes was used to give an indication of crack initiation and growth. The instant of crack initiation was determined from a plot of EP versus some indication of crack displacement, such as CMOD or rotation. A significant change in slope of the plot, during an increase in load, is an indication of crack initiation. However, the effects of dynamic reversing loads, such as existed in the inertial experiments, created significant ambiguities in the determination of crack initiation. Many separate data sets were plotted and cross plotted to assure a reasonable assessment of the point of crack initiation. The EP-CMOD data from Experiment 1.1-3, the stainless steel through-wall-cracked pipe test, are shown in Figure 3.26.

Figure 3-26. Plot of d-c electric potential versus crack-mouth-opening displacement for Experiment 1.1-3

DRB/1.1-3/F13R

The pressure transducer was used to determine the time at which the pressure boundary was lost. The output of this transducer was calibrated to yield 6.897 MPa/v (1,000 psi/v).

The displacements of the end masses for the inertial experiments were measured using the apparatus described earlier and which was calibrated to yield 204 mm/v (8.03 in/v) for the West unit and 208 mm/v (8.19 in/v) for the East unit, see Figure 3.1.

3.3 Results of Experiments

The results of these experiments are discussed below and are summarized in Table 3.5.

3.3.1 Uncracked-Pipe Experiment

The first experiments performed on the test system were conducted using an unflawed specimen. The primary purpose of these experiments was to shakedown the test system by checking out the system functions and data acquisition systems on a specimen which could be loaded repeatedly. The system was tested using a series of forcing functions designed to load the test frame above the maximum loads and displacements predicted by the pretest design for the cracked pipe experiments. These early experiments were also used to measure the natural frequencies of the piping system and to allow tuning of the servocontroller and hydraulic system.

During the course of these tests, modifications were required to eliminate system oscillations which excited unwanted mode shapes. Figure 3.27 shows the mechanical guide which was erected to eliminate horizontal oscillations of the end masses. A particularly difficult mode to eliminate was the second harmonic which was an asymmetric mode oscillating with one mass up and one mass down. The frequency of this mode was approximately 4.7 Hz and was so close to the frequency of the first mode, 4.2 Hz, that it was being excited along with the first mode when the forcing function was anything other than a pure sinusoid shape. This asymmetric mode was finally damped by connecting the hydraulic actuators to each other through a variable orifice. The upper chamber of each actuator was connected to the bottom chamber of the other actuator such that if the pipe began to be loaded in an asymmetric mode, there would be a restoring moment applied to eliminate the asymmetric loads.

Additional tests using the uncracked pipe specimen indicated that a better frequency to use for forcing the pipe/mass system was 3.5 Hz instead of the 3 Hz used in all the pretest design calculations. The system responded more crisply to the higher excitation frequency and required less oil to achieve a particular moment in the test section. The difference was due to the closer proximity of the faster forcing function to the first natural frequency of the test system.

The final shakedown test was performed with the pipe at 288 C (550 F) and with an internal pressure of 15.5 MPa (2,250 psi). The system was driven for 3 seconds using the increasing amplitude sinusoidal forcing function at 3.5 Hz, followed by a soft shutdown. This test attained moments in the test section estimated to be 113 kN-m (1,000,000 in-lb), causing general yielding in the pipe saddles and in the uniform bending moment region between the

Table 3.5. Summary of results from Subtask 1.1 cracked-pipe experiments

						Forcing Function ^(a) Parameters						
Experiment No.	Material	Outside Pipe Diameter, mm	Wall Thickness, mm	d/t	2c/ π D	Ψ , mm/sec	Freq., Hz	δ , mm/sec	Moment at Crack Initiation, kN-m	Maximum Moment, kN-m	Cycles or Time to Maximum Moment	Cycles Past Maximum Moment Until DEGB
1.1-2	Carbon Steel	167.5	14.4	1.0	0.370	2.0	3.5	—	32.9	38.8	30.2 cycles	3
1.1-3	Stainless Steel	169.0	14.0	1.0	0.370	25.0	3.5	—	24.8	29.0	3.4 cycles	2
1.1-4	Carbon Steel	167.8	13.2	0.502	0.516	4.0	3.5	—	64.3	75.6	35.6 cycles ^(b)	—
1.1-5	Stainless Steel	169.0	14.0	0.639	0.490	25.0	3.5	—	40.4	54.7	7.2 cycles	2
1.1-6	Carbon Steel	167.5	14.3	0.783	0.545	7.0	3.5	—	N.D.	48.6	15.2 cycles	1
1.1-7	Carbon Steel	167.5	13.5	0.647	0.432	—	—	152	75.3	77.2	0.42 seconds	—
1.1-8	Carbon Steel	167.4	14.1	0.758	0.424	7.0	3.5	—	N.D.	79.0	11.3 cycles	—
1.1-9	Carbon Steel	167.4	14.1	0.720	0.419	—	—	0.025	55.2	61.6	2488 seconds	—

N.D. = not determined.

(a) $\delta = \Psi t \sin(\omega t)$ for inertial experiments.

(b) Moment remained at maximum moment level for another 30 cycles before the surface crack penetrated the pipe wall.

Figure 3.27. View down length of specimen showing vertical track and wheels on mass

II.1-6/91-F3.27

saddles. The yielding in the region of the pipe saddles prompted a change to Schedule XXS pipe through the saddle grip area to ensure elastic response of the moment arms for the remainder of the tests.

3.3.2 Through-Wall-Cracked-Pipe Experiments

3.3.2.1 Experiment 1.1-2

This was the third cracked pipe inertial test performed in this test series and the second TWC experiment. The test specimen was a section of 6-inch nominal diameter, Schedule 120, A106 Grade B carbon steel pipe. The pipe specimen contained a circumferential TWC. This specimen was inertially loaded with a cyclic frequency of 3.5 Hz. The loads were induced by applying a sinusoidal displacement with an amplitude that increased linearly with time.

The specific objective of Experiment 1.1-2 was to evaluate the effects of inertial loading on the fracture behavior of carbon steel piping containing a circumferential TWC. This particular experiment was also the subject of an analytical predictive round-robin analysis conducted by several of the IPIRG members. The purpose of the analysis was to compare

predicted parameters such as cycles to failure and maximum moment with those obtained in the experiment.

Figure 3.28 is a plot of the electric potential (EP) at Crack Tip A* and crack section moment (divided by 100) as a function of the crack-mouth-opening displacement (CMOD) for Experiment 1.1-2. (The crack section moment data have been divided by 100 so that they would fit on the same plot as the EP data.) From this figure it can be seen that the slope of the EP-CMOD plot increases at a CMOD value of 0.65 mm (0.025 inch). This increase in slope is indicative of crack initiation. The moment at crack initiation (CMOD = 0.65 mm [0.025 inch]) is 32.9 kN-m (291,000 in-lb). This is 85 percent of the maximum moment for the experiment (38.8 kN-m [343,000 in-lb]).

3.3.2.2 Experiment 1.1-3

This was the first cracked-pipe inertial test conducted as part of Subtask 1.1. The test specimen was a section of 6-inch nominal diameter, Schedule 120 SA-376 TP304 stainless steel pipe. The pipe contained a circumferential TWC. This was the first pipe test to use crack rotation as a signal to shut down the hydraulic system. A relatively small rotation was set to trigger the shutdown sequence and, as a result, the system stopped loading shortly after maximum load had been achieved. The amount of crack growth attained in this test, Figure 3.29, was less than that in any of the tests that followed in which a larger rotation signal was used as the trigger.

Figure 3.30 is a plot of the EP at Crack Tip A as a function of CMOD for Experiment 1.1-3. (This is the same figure as shown previously in Figure 3.26.) From this figure it can be seen that the CMOD value at crack initiation was 6.4 mm (0.25 inch). Figure 3.31 is a plot of the crack section moment versus CMOD for Experiment 1.1-3. The moment at crack initiation is 24.8 kN-m (219,300 in-lb). This is 85 percent of the maximum moment for the experiment (29.0 kN-m [257,000 in-lb]).

3.3.3 Surface-Cracked-Pipe Experiments

3.3.3.1 Experiment 1.1-4

This was the second inertial surface-cracked pipe experiment conducted. The test specimen for this experiment was a section of 6-inch nominal diameter, Schedule 120, A106 Grade B carbon steel pipe. The pipe specimen contained a 180 degree circumferential external surface crack.

* The crack tips have been labeled A and B on the archived specimens.

Figure 3-28. Center span bending moment (data divided by 100) and electric potential at Crack Tip A versus crack-mouth-opening displacement (CMOD) for Experiment 1.1-2 (The CMOD data has been numerically filtered using a 10-point running average scheme.)
DRB/1.1-2/F15R

Figure 3.29. Post-test photograph of Specimen 1.1-3. The kink angle is 12 degrees and the crack growth is approximately 20 mm (0.8 inch) on each side
II.1/6/91/F3.29

Figure 3.30. Plot of d-c electric potential versus crack-mouth-opening displacement for Experiment 1.1-3

DRB/1.1-3/F13R

Figure 3.31. Moment versus crack-mouth-opening displacement for Experiment 1.1-3

DRB/1.1-3/F14R

AIM

Association for Information and Image Management

1100 Wayne Avenue, Suite 1100
Silver Spring, Maryland 20910
301/587-8202

Centimeter

Inches

MANUFACTURED TO AIM STANDARDS
BY APPLIED IMAGE, INC.

2 of 2

As was the practice for the carbon steel specimens, this specimen was subjected to cyclic loads prior to the experiment in order to initiate a fatigue crack, and thus sharpen the EDM starter notch. Unfortunately, post-test examination of the fracture surface revealed that no high cycle fatigue crack growth occurred. As a result, the initial flaw depth for the inertial loading portion of the experiment was much more shallow than planned. (In addition, due to the scalloped nature of the EDM starter flaw, the initial crack depth was very nonuniform, see Figure 3.32). The average crack depth-to-wall-thickness ratio of the scalloped notch prior to the start of the inertial loading was 0.518. This is considerably less than the target and assumed initial flaw depth of 66 percent of the pipe wall thickness. As a result of this shallow flaw, the design forcing function was not as aggressive as needed, and the number of cycles to failure was very large, 68. Figure 3.33 is a plot of the moment-time history for this experiment. This figure shows that about half of the applied load cycles were at the peak bending moment of 75.6 kN-m (669,000 in-lb). It can be seen that the moment-time history (Figure 3.33) followed the applied displacement-time history (Figure 3.34) in that applied displacements also leveled off. As a result of these 68 cycles, the crack grew by low-cycle fatigue to a depth of approximately 67 percent of the pipe wall thickness before failure occurred. This low-cycle fatigue crack growth during the inertial loading greatly complicates the analysis of this experiment.

3.3.3.2 Experiment 1.1-5

This was the first inertial surface-cracked-pipe experiment conducted. The test specimen was a section of 6-inch nominal diameter, Schedule 120, SA376 TP304 stainless steel pipe. The pipe contained a circumferential external surface crack.

Figure 3.35 is a plot of the EP at the crack centerline and center span bending moment (divided by 100) as a function of CMOD for Experiment 1.1-5. (The crack section moment data have been divided by 100 so that they would fit on the same plot as the EP data.) From this figure it can be seen that the CMOD value at crack initiation was 4.0 mm (0.16 inch). The moment corresponding to a CMOD of 4.0 mm (0.16 inch), i.e., the moment at crack initiation, is 40.4 kN-m (357,600 in-lb). This is 74 percent of the maximum moment. The maximum moment was 54.7 kN-m (484,000 in-lb).

3.3.3.3 Experiment 1.1-6

This was the third inertial surface-cracked-pipe experiment conducted. The test section for this experiment was a section of 6-inch nominal diameter, Schedule 120, A106 Grade B carbon steel pipe. The pipe specimen contained a circumferential 180 degree internal surface crack.

Figure 3.36 is a plot of the moment at the crack section versus time for Experiment 1.1-6. The maximum moment for this experiment was 48.6 kN-m (430,000 in-lb). There was no clip gage used in this experiment to measure CMOD and the rotation device failed. Consequently, due to the lack of CMOD or rotation data for this experiment, it is not possible to determine the instant of crack initiation.

Figure 3.32. This photograph of the fracture surface of Specimen 1.1-4 shows the external scalloped flaw as well as some small striations which are indicative of small incremental crack growth as the specimen was cycled to failure

Figure 3.33. Center span bending moment versus time for Experiment 1.1-4
DRB/1.1-4/F10R

Figure 3.34. Average displacement for East and West actuators versus time
for Experiment 1.1-4

DRB/1.1-4/F7R

Figure 3.35. Electric potential at crack centerline, as a function of centerline CMOD for Experiment 1.1-5. Also shown is a plot of center span bending moment. (In order to show the bending moment data on same figure, the bending moment data were divided by 100.) The change in slope of the EP versus CMOD data is indicative of crack initiation

DRB/1.1-5/F14R

Figure 3.36. Moment versus time for Experiment 1.1-6. (Moment data are the average of the moment data obtained from the strain gages located 25 and 175 mm (1- and 7-inches) from the crack plane)

DRB/1.1-6/F9R

3.3.3.4 Experiment 1.1-7

This was the dynamic monotonic, surface-cracked-pipe experiment. The test specimen for this experiment was a section of 6-inch nominal diameter, Schedule 120, A106 Grade B carbon steel pipe. The pipe specimen contained a circumferential 180 degree internal surface crack.

The specific objective of Experiment 1.1-7 was to develop experimental data for a carbon steel surface-cracked pipe subjected to dynamic monotonic loading conditions. The results from this experiment were compared with the results from the quasi-static, monotonic, carbon steel surface-cracked-pipe experiment in order to assess the effect of dynamic loading rates on the load-carrying capacity of a carbon steel specimen with a surface crack. Previous results from Subtask 1.2 (Refs. 3.2 and 3.3) indicated that the load-carrying capacity of a carbon steel specimen containing a through-wall crack was reduced approximately 6 to 8 percent at dynamic loading rates. This is somewhat less than what might be expected on the basis of tensile and C(T) specimen tests. The ultimate strength of this carbon steel material decreased approximately 25 percent at the higher loading rates, and the toughness (J) decreased approximately 15 percent (Ref. 3.4). The reduction in strength and toughness at the higher strain rates for this carbon steel material has been attributed to dynamic strain aging (DSA).

Another objective of this experiment was to validate the bending moment data derived from strain gage data by comparing those moments with moments derived from load cell data. Figure 3.37 is a plot of the crack section moment versus time for Experiment 1.1-7. Three curves are shown in Figure 3.37. One curve is based on load cell data where the average of the two load cell values has been multiplied by the moment arm length in order to calculate the moment within the center span. The other two curves are for moment data derived from strain gage data using the data reduction procedures specified previously in Equations 3.4 and 3.5. One curve (labeled "Inner Gages") is for a set of top and bottom gages within the center span, 150 mm (6 inches) from the crack plane. The other curve (labeled "Outer Gages") is for a set of top and bottom gages outside the center span, 864 mm (34 inches) from the crack plane. The moments for these outer gages were multiplied by the ratio of the moment arm length to the distance from these outer gages to the outer support point in order to correct for the fact that these gages are outside the region of uniform bending moment, see Figure 3.38. As a result, all three curves should reflect the bending moment at the crack section, within the region of constant bending moment. In examining Figure 3.37, it can be seen that the moments derived from the inner strain gages exhibit a significant amount of nonlinearity near the end of the test. This nonlinearity is associated with nonlinearities in one of the inner strain gages, possibly due to local yielding. The moments derived from the outer strain gages and load cells remained linear throughout the experiment. (Note, due to the fact that this was a constant loading rate experiment, the moment-time plot shown in Figure 3.37 will be the same shape as a moment-displacement plot. Consequently, the nonlinearities evident in Figure 3.37 will also be evident in a moment-displacement plot.) The moments from the outer strain gages are approximately 15 percent higher than the moments from the load cells at the end of the test. The moments derived from the load-cell data were used in the analyses of the two displacement-controlled experiments in Section 4 since it is thought that the load cell data

Figure 3.37. Moment-time plot for Experiment 1.1-7. Three curves are shown; one based on load cell data, one on inner strain gage data, and one on outer strain gage data.

DRB/1.1-7/F13

Figure 3.38. Strain gage locations for Experiments 1.1-7, 1.1-8, and 1.1-9
II.1-6/91-F3.38R

provide a better indication of the moments. The moments derived from the load-cell data are a more direct measurement since a number of assumptions must be imposed in calculating moments from strain-gage data. Figure 3.39 is a plot of the EP at the crack centerline versus the load-line displacement (LLD) for Experiment 1.1-7. From this figure it can be seen that the load-line displacement value at crack initiation, the point where the slope of the EP-LLD plot increases, was 50.3 mm (1.98 inches). Figure 3.40 is a plot of the crack section moment versus LLD for Experiment 1.1-7. The moments reported in this figure are based on the load cell data. The moment at crack initiation (LLD value of 50.3 mm [1.98 inches]) was 75.3 kN-m (666,000 in-lb). This is 98 percent of the maximum moment for the experiment. The maximum moment was 77.2 kN-m (683,000 in-lb).

3.3.3.5 Experiment 1.1-8

This was the final inertial surface-cracked-pipe experiment conducted. The test specimen for this experiment was a section of 6-inch nominal diameter, Schedule 120, A106 Grade B carbon steel pipe. The pipe contained an internal 180 degree surface crack.

The specific objective of this experiment was to repeat Experiment 1.1-6 as nearly as possible in order to ascertain the degree of repeatability between like experiments. Unfortunately, when the large amplitude dynamic displacements were applied to the cracked-pipe section during this inertial experiment, one of the two actuators did not respond to the controller command signal; it responded on an intermittent basis. Figure 3.41 is a plot of the displacement of the two actuators as a function of time for Experiment 1.1-8. As would be expected on the basis of the command signal, the displacement-time history for the west actuator is a smooth sinusoidal displacement function with a frequency of approximately 3.5 Hz. On the other hand, the displacement-time history for the East actuator looks like a random saw-tooth function with a frequency significantly less than 3.5 Hz. As a result, the displacement-controlled forcing function applied to the cracked-pipe section was very asymmetric.

Figure 3.42 is a plot of the moment at the crack section versus time for Experiment 1.1-8. These data are based on the inner strain gages. Due to the poor quality of the CMOD and rotation data, it was not possible to determine the instant of surface crack initiation for this experiment. The maximum moment for this experiment was 79.0 kN-m (699,000 in-lb).

3.3.3.6 Experiment 1.1-9

This was the quasi-static monotonic, surface-cracked pipe experiment. The test specimen for this experiment was a section of 6-inch nominal diameter, Schedule 120, A106 Grade B carbon steel pipe. The pipe specimen contained a circumferential 180 degree internal surface crack.

The results from this experiment were compared with results from other quasi-static monotonic, surface-cracked pipe experiments for this carbon steel material conducted in other programs, using different load frames, in order to assess the effect of the load frame, if any,

Figure 3.39. Centerline d-c EP versus actuator displacement for Experiment 1.1-7 DRB/1.1-7/F15R

Figure 3.40. Moment versus actuator displacement for Experiment 1.1-7 DRB/1.1-7/F16R

Figure 3.41. Displacement of the two actuators as a function of time for Experiment 1.1-8

Figure 3.42. Moment-time plot for Experiment 1.1-8
DRB/1.1-8/F14

on the experimental results. In addition, the results from this experiment were compared with the results from the dynamic monotonic, carbon steel, surface-crack experiment (1.1-7) to assess the effect of loading rate on the load-carrying capacity of a carbon steel specimen with a surface crack.

Another objective of this experiment was to validate the procedures for calculating bending moments from strain gage data much in the same fashion as was done for Experiment 1.1-7. Figure 3.43 is a plot of the crack section moment versus time for Experiment 1.1-9. Three curves are shown in Figure 3.43. One curve is based on load cell data while the other two curves are derived from strain gage data using the data reduction procedures specified previously in Equations 3.4 and 3.5. All three curves reflect the bending moment at the crack section, within the region of constant bending moment. In examining Figure 3.43, it can be seen that the moments derived from the inner strain gages are beginning to deviate from the moment data derived from the outer strain gages near the end of the test. The moment at which this deviation begins to occur (60 kN-m [531,000 in-lbs]) is approximately the same moment at which the inner and outer strain gage moment data began to deviate from one another for Experiment 1.1-7, see Figure 3.37. This supports the contention that the non-linearity has something to do with local yielding at the strain gage location. Again, the moments from the outer strain gages are approximately 15 percent higher than the moments from the load cells at the end of the test. As before, the load-cell moment data were used in the subsequent analyses of the experimental results for these displacement-controlled experiments in Section 4.

Figure 3.44 is a plot of the EP at the crack centerline as a function of the CMOD at the crack centerline for Experiment 1.1-9. The slope of the EP-CMOD plot increases, indicative of crack initiation, at a CMOD value of 0.53 mm (0.021 inch). From Figure 3.45 it can be seen that the moment at this value of CMOD, i.e., the moment at crack initiation, was 55.2 kN-m (488,680 in-lb). This is 90 percent of the maximum moment. The maximum moment was 61.6 kN-m (545,200 in-lb). The moments reported in Figure 3.45 are based on the load cell data.

3.4 References

- 3.1 Wilkowski, G. M., and others, "Degraded Piping Program-Phase II, Summary of Technical Results and Their Significance to Leak-Before-Break and In-Service Flaw Acceptance Criteria", March 1984-January 1989, by Battelle, NUREG/CR-4082, Vol. 8, March 1989.
- 3.2 Scott, P., Kramer, G., Veith, P., Francini, R., and Wilkowski, G., "The Effect of Dynamic and Cyclic Loading During Ductile Tearing on Circumferentially Cracked Pipe -- Experimental Results", ASME PVP Vol. 280, June 1994, pp 207-220.
- 3.3 Wilkowski, G., Kramer, G., Vieth, P., Francini, R., and Scott, P., "The Effect of Cyclic Loading During Ductile Tearing on Circumferentially Cracked Pipe -- Analytical Results", ASME PVP Vol. 280, June 1994, pp 221-240.

- 3.4 **Marschall, C. W., Landow, M. P., and Wilkowski, G. M., "Loading Rate Effects on Strength and Fracture Toughness of Pipe Steels Used in Task 1 of the IPIRG Program", NUREG/CR-6098, BMI-2175, Oct. 1993.**

Figure 3.43. Moment-time plot for Experiment 1.1-9. Three curves are shown; one based on load cell data, one on inner strain gage data, and one on outer strain gage data

DRB/1.1-9/F13

Figure 3.44. Centerline EP versus crack-mouth-opening displacement for Experiment 1.1-9

DRB/1.1-9/F16R

**Figure 3.45. Moment versus crack-mouth-opening displacement for Experiment 1.1-9
I1.1-6/91-F3.45**

4. ANALYSIS OF CRACKED-PIPE FRACTURE EXPERIMENTS

This section of the report describes and presents results of different analysis methods to predict the fracture behavior of the inertial and displacement-controlled cracked pipe experiments of Subtask 1.1 of the IPIRG Program. Three different types of analysis methods were evaluated. The first type of engineering analysis methods that were evaluated predict maximum loads and loads at crack initiation in some cases. These were:

- Net-Section-Collapse (NSC) analysis
- Dimensionless Plastic-Zone Parameter (DPZP) analysis
- J-estimation schemes
 - LBB.ENG2 and GE/EPRI for the through-wall-cracked pipe experiments
 - SC.TNP for the surface-cracked pipe experiments
- R6 analysis using the Revision 3 Option 1 method
- ASME Section XI flaw evaluation criteria
 - IWB-3640 for austenitic steels
 - IWB-3650 for ferritic steels

The first three of these methods (i.e., NSC, DPZP, and J-estimation schemes) could be considered as predictive analyses while the last two (R6 and ASME Section XI) are more fracture prevention criteria.

The second type of analysis method involved calculations of the value of J at crack initiation from the surface-cracked-pipe experiments. In some through-wall-cracked pipe cases, the J-R curve was calculated. Both calculations used η -factor analyses.

The third type of analysis involved dynamic analysis using the ANSYS finite element code with the spring-slider representation of the crack to predict time-history behavior up to maximum load.

Table 4.1 is a summary table showing the ratio of the maximum experimental stress to the predicted stress (including the axial stress contribution due to internal pressure) using the different predictive fracture analysis methods considered for the Subtask 1.1 experiments.

Table 4.1. Summary table showing ratio of maximum experimental stresses to predicted stresses for Subtask 1.1 experiments for the different fracture analysis methods considered

Expt. No.	Crack Geometry	Material	Loading	Experimental Stress/Predicted Stress ^(a)							
				NSC ^(b)	DPZF ^(b)	LBB.ENG2 ^(c)	GE/EPRI ^(c)	SC.TNP	R6 ^(c,d)	ASME S _m (Code)	ASME S _m (Actual)
1.1-2	TWC	A106B	Inertial	0.82	1.56	1.15	1.25		1.69	1.93	1.34
1.1-3	TWC	TP304	Inertial	1.09	1.09	1.51	1.60		3.93	1.05	0.98
1.1-5	XSC	TP304	Inertial	1.17	1.17			1.24	2.40	1.10	1.03
1.1-6	ISC	A106B	Inertial	0.96	1.07			N.D. ^(e)	2.79	2.30	1.58
1.1-7	ISC	A106B	Dyn Mono	0.97	1.08			1.15	1.42	2.13	1.51
1.1-8	ISC	A106B	Inertial	1.13	1.25			N.D.	N.D.	2.51	1.77
1.1-9	ISC	A106B	QS Mono	0.84	0.93			N.D.	1.27	1.87	1.32

(a) Experimental axial plus bending stress divided by predicted axial plus bending stress.

(b) Flow stress = average of the yield and ultimate strengths.

(c) Using quasi-static J_D-R curve.

(d) Ratio of stress from an elastic stress analysis to the predicted stress.

(e) N.D. -- not determined

4.1 Crack Initiation and Maximum Load Predictive Analyses

4.1.1 Net-Section-Collapse Analyses

The Net-Section-Collapse (NSC) analysis is a limit-load analysis that was first proposed for application to stainless steel pipe with circumferential through-wall cracks, Ref. 4.1. It was subsequently verified for different diameter stainless steel pipes with circumferential surface and through-wall cracks, Ref. 4.2.

The Net-Section-Collapse analysis assumes the material is sufficiently tough that there is negligible crack growth prior to reaching maximum load. It also assumes the pipe cross section becomes fully plastic and the net-section stress reaches a constant value. The critical net-section stress at maximum load is called the flow stress.

Several different definitions of flow stress have been used in the Net-Section-Collapse analysis. The most frequently used value is the average of the yield and ultimate strengths. However, experimental results from the Degraded Piping Program, Ref. 4.3, have shown that the average value of flow stress calculated by the Net-Section-Collapse equations (see Appendix B) is 1.15 times the average of the yield and ultimate strengths. Using just the average of the yield and ultimate strengths gives a predicted failure stress of approximately two standard deviations below the average pipe test results. Consequently, using the average of the yield and ultimate strengths for the flow stress is slightly conservative.

Another definition of flow stress comes from the ASME Section XI flaw evaluation procedures, Refs. 4.4 and 4.5. Here, the flow stress is defined as $3S_m$ for stainless steels, and $2.4S_m$ for ferritic steels, where S_m is the design stress intensity from Section III of the ASME Boiler and Pressure Vessel code.

Although there are controversies on modification to the Net-Section-Collapse analysis for either short through-wall cracks (Refs. 4.2 and 4.6), or for short but deep surface cracks (Refs. 4.7 and 4.8), the basic equations given in Appendix B of this report are believed to be applicable for the relatively long cracks evaluated in this effort.

Tables 4.2a and 4.2b compare the predicted stresses from the Net-Section-Collapse analysis with the experimental stresses. Table 4.2a is for the stainless steel experiments and Table 4.2b is for the carbon steel experiments. Tables 4.2a and 4.2b present the predictions for each of the three definitions of flow stress previously discussed; i.e., the average of the yield and ultimate strengths, 1.15 times the average of the yield and ultimate strengths, and either $3S_m$ for the austenitic experiments or $2.4S_m$ for the ferritic experiments. The results of the comparisons from Tables 4.2a and 4.2b are shown in graphical form in Figures 4.1 through 4.3.

For the TP304 stainless steel pipe experiments, the experimentally measured stresses are slightly above the stresses predicted by Net-Section-Collapse analysis when the flow stress is defined as the average of the yield and ultimate strengths. This finding is consistent with the findings from quasi-static pipe experiments from the Degraded Piping Program (Ref. 4.3) where it was shown that using the average of the yield and ultimate strengths for flow stress resulted in slight underpredictions of the experimental results. For these experiments, when using the ASME code definition of the flow stress, $3S_m$, the Net-Section-Collapse analysis overpredicted the experimental results.

For the A106 Grade B pipe experiments, when actual measured tensile properties (either the average of the yield and ultimate strengths or 1.15 times the average) are used to define the flow stress, then the experimental stresses tended to fall below the NSC predicted stress. This is to be expected. The toughness of this carbon steel material is sufficiently low that one of the basic assumptions of the Net-Section-Collapse analysis, i.e., the cracked cross section reaches fully plastic conditions, is being violated. Conversely, when the NSC predicted stresses are based on the ASME Code definition of flow stress, $2.4S_m$, the experimental stresses are above the NSC predicted stresses. This is an artifact of the actual strength (flow stress) of this particular carbon steel being significantly higher than expected on the basis of Section III Code values for S_m .

It is also of note from Tables 4.2a and 4.2b and Figures 4.1 through 4.3 that the ratios of the experimental stresses to the predicted stresses were lower for the through-wall-cracked pipe experiments than for the surface-cracked pipe experiments. This was true for both the stainless and carbon steel experiments. Past test results showed that through-wall-cracked pipe is more sensitive to toughness than surface-cracked pipe, see Figure 4.4.

4.1.2 Dimensionless Plastic-Zone Screening Criterion

In order to identify conditions where fully plastic conditions exist (that is, where the NSC analysis should be appropriate), a Dimensionless Plastic-Zone Screening Criterion was developed as part of the Degraded Piping Program (Ref. 4.9). The Dimensionless Plastic-Zone Screening Criterion is a semi-empirical relationship in which the ratio of the maximum predicted stress to the calculated NSC stress is a function of a Dimensionless Plastic-Zone Parameter (DPZP). The DPZP is the ratio of the Irwin plastic-zone size to either the distance from the crack tip (for through-wall cracks) or the crack centerline (for surface cracks) to the neutral axis. This relationship is shown in Figure 4.4 with a large amount of experimental data. In this figure, the NSC stress is based on a flow stress equal to the average of the actual yield and ultimate strengths of the material.

In Figure 4.4, the ratio of the experimental stress to the predicted NSC stress is related to the DPZP by

$$P/P_{nsc} = (2/\pi) \arccos (e^{-C'(DPZP)}), \quad (4.1)$$

Table 4.2a. Comparison of Net-Section-Collapse analysis predictions with Subtask 1.1 experimental maximum stresses for stainless steel pipe experiments

	Experiment 1.1-3 Through-Wall Crack	Experiment 1.1-5 External Surface Crack
Flow Stress Definitions		
$\sigma_{f1} = (\sigma_y + \sigma_w)/2$, MPa	293.9	293.9
$\sigma_{f2} = 1.15 (\sigma_y + \sigma_w)/2$, MPa	337.9	337.9
$\sigma_{f3} = 3S_m$, MPa	350.4	350.4
Predicted Bending Stress		
NSC (σ_{f1}), MPa	105.8	186.2
NSC (σ_{f2}), MPa	129.4	221.1
NSC (σ_{f3}), MPa	136.0	230.6
Experimental Results		
Axial Stress, MPa	34.7	34.7
Bending Stress, MPa	119.0	224.1
Experimental/Predicted Maximum Stresses^(a)		
Experiment/NSC (σ_{f1})	1.09	1.17
Experiment/NSC (σ_{f2})	0.94	1.01
Experiment/NSC (σ_{f3})	0.90	0.97

(a) Experimental axial plus bending stress divided by predicted axial plus bending stress.

Table 4.2b. Comparison of Net-Section-Collapse analysis predictions with Subtask 1.1 experimental maximum stresses for carbon steel pipe experiments

	Exp. 1.1-2 Through-Wall Crack	Exp. 1.1-6 Surface Crack	Exp. 1.1-7 Surface Crack	Exp. 1.1-8 Surface Crack	Exp. 1.1-9 Surface Crack
Flow Stress Definitions					
$\sigma_{f1} = (\sigma_y + \sigma_w)/2$, MPa	469.9	469.9	469.9	469.9	469.9
$\sigma_{f2} = 1.15 (\sigma_y + \sigma_w)/2$, MPa	540.4	540.4	540.4	540.4	540.4
$\sigma_{f3} = 2.4S_m$, MPa	299.3	299.3	299.3	299.3	299.3
Predicted Bending Stress					
NSC (σ_{f1}), MPa	200.9	207.9	342.8	288.7	312.9
NSC (σ_{f2}), MPa	238.0	246.9	400.4	338.4	366.0
NSC (σ_{f3}), MPa	110.2	112.9	202.3	167.4	183.5
Experimental Results					
Axial Stress, MPa	33.9	34.1	36.5	34.5	34.5
Bending Stress, MPa	159.0	200.0	331.3	329.7	257.1
Experimental/Predicted Maximum Stresses^(a)					
Experiment/NSC (σ_{f1})	0.82	0.96	0.97	1.13	0.84
Experiment/NSC (σ_{f2})	0.71	0.83	0.84	0.98	0.73
Experiment/NSC (σ_{f3})	1.34	1.59	1.54	1.80	1.34

(a) Experimental axial plus bending stress divided by predicted axial plus bending stress.

Figure 4.1. Bar chart showing the ratio of the maximum experimental stress to the NSC predicted stress. (The flow stress definition for this figure is the average of the yield and ultimate strengths of the material.)

11.2-6/91-F4.1

Figure 4.2. Bar chart showing the ratio of the maximum experimental stress to the NSC predicted stress. (The flow stress definition for this figure is 1.15 times the average of the yield and ultimate strengths of the material.)

11.2-6/91-F4.2

Figure 4.3. Bar chart showing the ratio of the maximum experimental stress to the NSC predicted stress. (The flow stress definition for this figure is $3S_m$ for the stainless steel experiments and $2.4S_m$ for the carbon steel experiments.)

11.2-6/91-F4.3

where

DPZP	=	$2EJ_i / (\pi^2 \sigma_f^2 D)$,	(4.2)
P	=	total failure stress,	
P_{NSC}	=	NSC predicted tension and bending stress,	
E	=	elastic modulus,	
J_i	=	J at crack initiation (may be J_k),	
σ_f	=	flow stress = $(\sigma_y + \sigma_u)/2$,	
D	=	nominal pipe diameter, and	
C'	=	statistically based parameter.	

The factor "C" is determined from a statistical fit of Equation 4.1 to pipe fracture data. The upper curve in Figure 4.4 represents a best fit of the surface-cracked pipe data. The value of "C" for this curve is 21.8. The lower curve represents a best fit of the through-wall-cracked pipe data. The value of "C" for this curve is 4.62.

The maximum bending stresses predicted from the Dimensionless Plastic-Zone Screening Criterion are compared with the experimental stresses in Tables 4.3a and 4.3b for the stainless and carbon steel experiments, respectively. These comparisons are shown in graphical form in Figure 4.5.

For the stainless steel pipe experiments, the value of DPZP is approximately 1.5 when using the quasi-static material properties. Hence, Net-Section-Collapse conditions should be satisfied, and the results for the DPZP analysis (Table 4.3a) should be the same as for the NSC analysis (Table 4.2a) for the case where the flow stress is defined as the average of the yield and ultimate strengths. This is indeed the case.

Figure 4.4. Ratio of maximum experimental stress to predicted Net-Section-Collapse stress as a function of the Dimensionless Plastic-Zone Parameter. (The flow stress definition for this figure is the average of the yield and ultimate strengths.)

11.2-8/89-F4.4

For the carbon steel pipe experiments, the value of DPZP is 0.085 when using the quasi-static material properties. Hence, Net-Section-Collapse conditions should not be satisfied. For these experiments, the DPZP analysis will lower the predicted stress and the ratio of the experimental stresses to the predicted stress should be higher for the DPZP analysis (Table 4.3b) than for the NSC analysis (Table 4.2b) for the case where the flow stress is defined as the average of the yield and ultimate strengths. Again, this is the case.

In examining Table 4.3 and Figure 4.5, it can be seen that the Dimensionless Plastic-Zone Screening Criterion underpredicted the maximum experimental stresses in all but one experiment. The one exception was Experiment 1.1-9 which was the quasi-static monotonic, carbon steel surface-cracked pipe experiment. The ratio of the maximum experimental stress to the DPZP predicted stress for this experiment was 0.93. This value is well within the experimental scatter band, especially for this carbon steel. One may expect significant variations in tensile properties for ferritic materials, such as evaluated in these experiments, which are susceptible to dynamic strain aging. As part of the NRC's "Short Cracks in Piping and Piping Welds" program it was observed that there was a significant amount of scatter in the elevated temperature hardness data for this particular heat of carbon steel. This variation in hardness data would translate to scatter in the tensile data, which further implies variability in the results of the pipe tests.

Figure 4.5. Bar chart showing the ratio of the maximum experimental stress to the DPZP predicted stress. (The flow stress definition for this figure is the average of the yield and ultimate strengths.)

11.2-6/91-F4.5

Table 4.3a. Comparison of Dimensionless Plastic-Zone Screening Criterion predictions with Subtask 1.1 experimental maximum stresses for stainless steel pipe experiments

	Experiment 1.1-3 Through-Wall Crack	Experiment 1.1-5 External Surface Crack
Flow Stress Definition		
$\sigma_f = (\sigma_y + \sigma_u)/2$, MPa	293.9	293.9
Predicted Bending Stress	105.8	186.2
Experimental Results		
Axial Stress, MPa	34.7	34.7
Bending Stress, MPa	119.0	224.1
Experimental/Predicted Maximum Stresses ^(a)	1.09	1.17

(a) Experimental axial plus bending stress divided by predicted axial plus bending stress.

Table 4.3b. Comparison of Dimensionless Plastic-Zone Screening Criterion predictions with Subtask 1.1 experimental maximum stresses for carbon steel pipe experiments

	Exp. 1.1-2 Through-Wall Crack	Exp. 1.1-6 Surface Crack	Exp. 1.1-7 Surface Crack	Exp. 1.1-8 Surface Crack	Exp. 1.1-9 Surface Crack
Flow Stress Definition					
$\sigma_f = (\sigma_y + \sigma_u)/2$, MPa	469.9	469.9	469.9	469.9	469.9
Predicted Bending Stress, MPa	89.9	183.5	304.6	256.1	278.0
Experimental Results					
Axial Stress, MPa	33.9	34.1	36.5	34.5	34.5
Bending Stress, MPa	159.0	200.0	331.3	329.7	257.1
Experimental/Predicted Maximum Stresses ^(a)	1.56	1.07	1.08	1.25	0.93

(a) Experimental axial plus bending stress divided by predicted axial plus bending stress.

4.1.3 J-Estimation Scheme Analyses

For cases where limit-load analyses are inappropriate due to low toughness or large diameter pipe, elastic-plastic analysis may be needed to obtain more accurate and slightly conservative estimates of the load at crack initiation and the maximum load.

Various methods exist to predict the maximum load for circumferentially cracked pipe in pure bending. Detailed finite element analyses or simpler approximate closed-form solutions can be used. Detailed finite element analysis could be used along with the material fracture resistance curve to determine the strength of the cracked pipe during crack growth. Many different fracture mechanics parameters have been proposed, but the J-integral parameter is the most frequently used parameter in the nuclear industry. Several closed-form solutions for J exist that give approximate elastic-plastic fracture mechanics solutions for circumferentially cracked pipe. These are frequently referred to as J-estimation schemes. Applications of these estimation schemes to through-wall-cracked pipe and surface-cracked pipe are described in the following.

4.1.3.1 Material Property Input for J-Estimation Scheme Analyses

The material property data needed for input to these analyses are:

- the elastic modulus, yield and ultimate strengths,
- the Ramberg-Osgood fit parameters of the stress-strain curve, and
- the J-R curve of the material.

Tensile Test Data. The yield and ultimate strength of the materials under quasi-static and dynamic loading rates (strain rates of approximately 1 to 10 sec⁻¹) were documented in Section 2 of this report. The elastic modulus was taken from handbook values, since it could not be accurately determined from the tensile test data which were developed with a large-displacement extensometer.

Ramberg-Osgood Fits of Stress-Strain Curves. The stress-strain curves obtained from both the quasi-static and dynamic tests were used to determine the Ramberg-Osgood constants. This was done using a program called ROFIT developed at Battelle. The fit of the data that gave the best regression coefficient was used. The stainless steel curves are shown in Figures 4.6a and 4.6b. The A106 Grade B stress-strain curves and the experimental data used in the fits are shown in Figures 4.7a and 4.7b. The Ramberg-Osgood coefficients are given in the figures.

Extrapolated J-R Curves. The J-R curves used in the pipe predictions were from both the quasi-static and dynamic, monotonic, side-grooved C(T) specimen tests, see Section 2 of this report. The C(T) specimens were machined from the L-C orientation which would correspond to the growth of a circumferential through-wall crack. Both the J_D -R and J_M -R curves were used. For the analyses of the surface-cracked pipe experiments, there is very little crack growth from initiation to maximum load, hence it does not practically matter if the J_D - or J_M -R curves are used. For the through-wall-cracked pipe experiments, the J-R curves had to be extrapolated since the crack growth from the C(T) specimens was too small to reach the values from the pipe experiments. For the J_D -R curve the following power-law extrapolation procedure from Reference 4.9 was used.

(a) Quasi-static tensile test A23-105 at $\dot{\epsilon} = 5.5 \times 10^{-4} \text{ sec}^{-1}$
(Regression coefficient = 0.9913)

(b) Dynamic tensile test A23-101 at $\dot{\epsilon} = 1.1 \text{ sec}^{-1}$
(Regression coefficient = 0.9926)

Figure 4.6. Ramberg-Osgood fits of stainless steel Pipe A23 tensile test data at 288 C (550 F)

11.2-8/89-F4.5

(a) Quasi-static tensile test F30-104 at $\dot{\epsilon} = 4 \times 10^{-4} \text{ sec}^{-1}$
(Regression coefficient = 0.9937)

(b) Dynamic tensile test F30-101 at $\dot{\epsilon} = 1.6 \text{ sec}^{-1}$
(Regression coefficient = 0.8646)

Figure 4.7. Ramberg-Osgood fits of A106 Grade B Pipe F30 tensile test data at 288 C (550 F)

11.2-8/89-F4.6

- (a) The experimental J_D -R curve data were used for crack growth of up to 30-percent of the C(T) specimen ligament. These data were fit with a power-law curve,

$$J = c' \Delta a^m. \quad (4.3)$$

- (b) The NRCPIPE code requires a J-R curve that starts with J_{Ic} . Hence, it was necessary to modify Eq. 4.3 to

$$J = J_{Ic} + c'' \Delta a^{m'} \quad (4.4)$$

The J_M -R curve data were linearly extrapolated using the data for crack growth between 10- and 30-percent of the specimen ligament. This is schematically illustrated in Figure 4.8, and is discussed further in Reference 4.9.

The extrapolated J-R curves used for the TP304 stainless steel pipe material used in the Subtask 1.1 pipe experiments are shown in Figures 4.9a and 4.9b for quasi-static and dynamic loading cases, respectively. The extrapolated J-R curves used for the A106 Grade B pipe material used in the Subtask 1.1 pipe experiments are shown in Figures 4.10a and 4.10b for the static and dynamic loading cases, respectively.

Figure 4.8. Schematic of different J-R curve extrapolation methods
I1.2-8/89/F4.7

(a) Quasi-static (Specimen A23-113)

(b) Dynamic (average of Specimens A23-108 to A23-112)

Figure 4.9. Extrapolated J-R curves for stainless steel Pipe A23 at 288 C (550 F) used in NRCPIPE calculations
I1.2-8/89/F4.8

(a) Quasi-static

(b) Dynamic

Figure 4.10. Extrapolated J-R curves for A106 Grade B Pipe F30 at 288 C (550 F) used in J-estimation scheme calculations

I1.2-8/89/F4.9R

4.1.3.2 Analysis of Through-Wall-Cracked-Pipe Experiments

In the Degraded Piping Program, Ref. 4.9, an IBM-PC compatible computer code called NRCPIPE was written to perform various J-estimation scheme calculations. Some of the J-estimation schemes for through-wall circumferentially cracked pipe that exist in the NRCPIPE code are;

- GE/EPRI (Ref 4.10)
 - Original NP-1931
 - Original NP-1931 without plastic-zone correction in the J_e term
 - α modification of Zahoor, NP-4883M (Ref 4.11),
- Paris/Tada, NUREG/CR-3464 (Ref 4.12),
- LBB.NRC, NUREG/CR-4572 (Ref 4.13),
- LBB.GE, NUREG/CR-4853 (Ref 4.14),
- LBB.ENG
 - LBB.ENG1, NUREG/CR-4853 (Ref 4.14)
 - LBB.ENG2 (Ref 4.15), and
- R6 Revision 3 Option 1 - bending only (Ref 4.16).

A detailed discussion of these analyses can be found in Reference 4.14, with the exception of the R6 method which is described in Reference 4.16. Of these methods, only the GE/EPRI, LBB.ENG2, and R6 Option 1 methods were used to predict the initiation and maximum loads for the Subtask 1.1 pipe experiments. The GE/EPRI method was chosen because it is the most widely used method in the industry. The LBB.ENG2 method was chosen because past experience indicates that it is the most accurate of the various methods (Ref. 4.15).

The option to analyze a pipe subjected to combined pressure and bending using the GE/EPRI functions is described first. With this option, J is evaluated as:

$$J = J_e + J_p \quad (4.5)$$

J_e is evaluated in the usual fashion as:

$$J_e = \frac{(K_{Im} + K_{Ib})^2}{E} \quad (4.6)$$

where K_{Im} and K_{Ib} are the membrane and bending values of the stress intensity factor, respectively, and E is the elastic modulus. In the GE/EPRI analysis calculations, the original assumption of a plastic-zone correction on J_e was used.

J_p is calculated as:

$$J_p = \alpha \sigma_o \epsilon_o a \left[1 - \frac{\theta}{\pi} \right] \hat{h}_1 \left[\frac{M}{M_o} \right]^{n+1} \quad (4.7)$$

where

α , n , ϵ_o , and σ_o are the usual Ramberg-Osgood parameters,
 a is the half-crack length,
 θ is the half-crack angle,
 M is the bending moment,
 M_o is the limit moment (based on σ_o) for combined bending and pressure, and

$$\hat{h}_1 = h_1^B + \left[\frac{\sigma'_T}{\sigma'_L} \right] h_1^T. \quad (4.8)$$

Here,

σ'_T is the applied tension stress (due to pressure), and

σ'_L is the limit stress (based on flow stress) for combined bending and tension.

h_1^B is the GE/EPRI influence functions for through-wall-cracked pipe under pure bending, and

h_1^T is the GE/EPRI influence functions for through-wall-cracked pipe under pure tension.

Note that this procedure differs from that of the GE/EPRI method for combined loading (NP-5596, Ref. 4.17). The NP-5596 method has not yet been programmed into NRCPIPE.

The LBB.ENG2 method is a circumferential through-wall-cracked pipe J-estimation scheme developed at Battelle as part of the Degraded Piping Program as an alternative to the GE/EPRI (Ref. 4.10), Paris/Tada (Ref. 4.12), and LBB.NRC (Ref. 4.13) methods. The through-wall crack in the LBB.ENG2 method is represented by a pipe section with a reduced wall thickness which is centered on the crack plane. The reduced thickness section is an attempt to simulate the reduction in system compliance due to the crack. It is assumed that deformation theory plasticity controls the stress-strain response and that beam theory assumptions hold. Further details on the derivation of the LBB.ENG2 method are provided in Reference 4.14.

In addition to evaluating the GE/EPRI and LBB.ENG2 methods, the R6 Revision 3, Option 1 approach was also evaluated. The results of that analysis are discussed in Section 4.1.4 of this report.

4.1.3.3 Results of Through-Wall-Cracked-Pipe J-Estimation Scheme Predictions

In this section of the report predictions of stresses at both crack initiation and maximum load are made for the GE/EPRI and LBB.ENG2 J-estimation schemes. Predictions have been made using both quasi-static and dynamic material property data as well as using both the J_D -R and J_M -R curves.

Crack Initiation. Comparisons of the experimental-to-predicted stresses at crack initiation for the two through-wall-cracked pipe experiments are shown in Table 4.4 and Figure 4.11. These comparisons were made for predictions using both the quasi-static and dynamic material properties. Note that at crack initiation, J_D and J_M are the same, so it does not matter which J-R curve is used to predict crack initiation loads.

Table 4.4 and Figure 4.11 show that there was very little difference in the predictions of crack initiation stresses between the two methods (LBB.ENG2 and GE/EPRI). It is also evident that the

Figure 4.11. Bar chart showing the ratio of the experimental crack initiation stress to the GE/EPRI and LBB.ENG2 predictions for the Subtask 1.1 through-wall-cracked-pipe experiments

II.1-6/91-F4.11

Table 4.4. J-estimation scheme predictions of crack initiation stresses for Subtask 1.1 through-wall-cracked-pipe experiments

Experiment No.	Experimental Stress, MPa ^(a)	Predicted Stress, MPa ^(a)		Experimental Stress ^(a) Predicted Stress ^(a)		Quasi-Static or Dynamic Properties
		GE/EPRI	LBB.ENG2	GE/EPRI	LBB.ENG2	
1.1-2	168.6	114.1	114.2	1.48	1.48	QS
1.1-3	137.1	92.3	93.0	1.49	1.47	QS
1.1-2	168.6	104.1	100.2	1.62	1.68	DYN
1.1-3	137.1	88.0	88.3	1.56	1.55	DYN

(a) Includes axial stress contribution due to internal pipe pressure.

Table 4.5. J-estimation scheme predictions of maximum stresses for Subtask 1.1 through-wall-cracked-pipe experiments

Experiment No.	Experimental Stress, MPa ^(a)	Predicted Stress, MPa ^(a)		Experimental Stress ^(a) Predicted Stress ^(a)		Quasi-Static or Dynamic Properties	J _D - or J _M -R Curve Used
		GE/EPRI	LBB.ENG2	GE/EPRI	LBB.ENG2		
1.1-2	192.9	154.5	167.7	1.25	1.15	QS	J _D
1.1-3	153.7	95.8	102.1	1.60	1.51	QS	J _D
1.1-2	192.9	144.1	153.5	1.34	1.27	DYN	J _D
1.1-3	153.7	95.5	112.4	1.61	1.37	DYN	J _D
1.1-2	192.9	161.9	178.0	1.19	1.08	QS	J _M
1.1-3	153.7	101.3	116.2	1.52	1.32	QS	J _M
1.1-2	192.9	146.4	156.5	1.32	1.23	DYN	J _M
1.1-3	153.7	100.1	122.6	1.54	1.25	DYN	J _M

(a) Includes axial stress contribution due to internal pipe pressure.

experimental stresses at crack initiation are higher than the predicted values. This was true for both cases where quasi-static and dynamic material property data were used in the analyses. The use of dynamic material data slightly lowered the predicted crack initiation stresses for both materials. This is attributed to lower ultimate strengths, and thus lower flow stresses, for these materials at dynamic loading rates, see Tables 2.1 and 2.2.

Maximum Load. Comparisons of the experimental-to-predicted stress ratios at maximum load for the through-wall-cracked pipe experiments are shown in Table 4.5 and Figure 4.12. These comparisons were made for GE/EPRI and LBB.ENG2 predictions using both the quasi-static and dynamic material properties and both J_D -R and J_M -R curves.

In all cases, the predicted stresses were less than the experimental stresses. The GE/EPRI method tended to give lower predictions than the LBB.ENG2 method. This is consistent with what has been reported previously (Ref. 4.9). The use of dynamic material property data had almost no effect on the GE/EPRI predictions for the stainless steel experiment (1.1-3) and little effect (5-10 percent increase) on the LBB.ENG2 predictions for this experiment. This slight increase is attributed to the higher J-R curve for this stainless steel material at dynamic loading rates (see Figure 2.15). For the carbon steel experiments the use of dynamic material data also had little effect (5 to 15 percent) on the predictions for both methods considered. The use of dynamic properties slightly decreased the predicted stresses for both the LBB.ENG2 and GE/EPRI methods. This is expected, since the dynamic loading rates lowered the yield and ultimate strengths of this material (see Table 2.1) and also lowered the J-R curve (see Figure 2.12). The use of the J_M -R curve results in slightly higher predicted stresses than the use of the J_D -R curve.

4.1.3.4 Results of Surface-Cracked-Pipe J-Estimation Scheme Analyses

There are very few finite-length circumferential surface-cracked pipe J-estimation scheme analyses available. Some of the existing methods are;

GE/EPRI	-	EPRI report NP-5596, Ref. 4.17
R6	-	CEGB report R/H/R6-Rev. 3, Ref. 4.16, and
SC.TNP and SC.TKP	-	NRC report NUREG/CR-4872, Ref. 4.18.

The GE/EPRI method uses an h_1 function to calculate the plastic contribution of J. The h_1 values are calculated from finite element analyses. Currently, there are very few values of h_1 for combined tension and bending. In fact, for the experiments conducted in this program, it was not possible to use this analysis since there were no h_1 values available for interpolation for surface-crack lengths of 180 degrees.

The R6 Revision 3 Option 1 method is discussed in detail in Section 4.1.4 of this report. There are three options in R6 Revision 3. Option 3 provides for the use of any elastic-plastic fracture mechanics analyses such as the SC.TNP and SC.TKP J-estimation schemes. The SC.TNP and SC.TKP solutions were developed in the NRC Degraded Piping Program, Ref. 4.18. These solutions use the GE/EPRI 360-degree surface-cracked pipe solution for pure tension to develop new h-functions for use in the calculation of the plastic component of J, J_p , for finite-length

Figure 4.12. Bar chart showing the ratio of the maximum experimental stresses to the GE/EPRI and LBB.ENG2 predicted stresses for the Subtask 1.1 through-wall-cracked-pipe experiments

II.1-6/91-F4.12

surface-cracked pipe in bending. These solutions were implemented into the NRCPIPE Code during the Degraded Piping Program, and hence were available to assess the experimental data. Since past results showed that more accuracy is obtained using the SC.TNP solution, only this analysis procedure was used to assess these data.

Modification of SC.TNP for Tension and Bending. The prediction of initiation and maximum moment stresses for the circumferentially surface-cracked-pipe experiments in this study is complicated by the combined tension and bending loads, as well as the load reversals prior to crack initiation. A predictive analysis method should include the effect of load reversals both on the crack driving force (i.e., applied J) and on the material's crack growth resistance (i.e., J - R curve). However, in the present analysis, the reverse loading effects are ignored.

Another potential limitation with the method in its current form is that the method does not include the effect of internal pressure or tension on J . Currently, the effect of internal pressure is addressed in an approximate way, by ignoring possible loading path effects on plastic deformation, and replacing the axial tension in the uncracked ligament ahead of the crack by an additional applied moment. The equivalent moment, M_{eq} , from the pressure-induced axial tensile stress is taken to be equal to the difference between the Net-Section-Collapse predicted moment under pure bending and the Net-Section-Collapse predicted moment under combined bending and tension from internal pressure.

Even with the above simplifications, the prediction of surface-cracked pipe behavior is a difficult analysis problem awaiting further research. At present the SC.TNP method appears to be the most complete method available, short of full three-dimensional finite-element analysis. The key equation from Reference 4.18 is the plastic component of J , J_p , which is given by:

$$J_p = \alpha \epsilon_0 \sigma_0 (1 - a/t) a h_1 (k\sigma/P_0)^{n+1}. \quad (4.9)$$

The h_1 function corresponds to an axisymmetrically cracked pipe loaded in tension and α , ϵ_0 , n , and σ_0 are the constants corresponding to a Ramberg-Osgood representation of the material's uniaxial (tension) stress-strain curve. P_0 is the limit-load corresponding to the reference stress σ_0 . The stress, σ_1 is the nominal stress, the stress away from the crack. As discussed in Reference 4.18, the fact that there is less than a 360-degree surface crack and that the pipe is subjected to remote bending (rather than tension) is included in the analysis by modifying the applied stress (σ) term in Eq. 4.9. Also, the SC.TNP solutions are for an internal surface crack, but could be modified for an external surface crack.

With the above approximations and assumptions, analyses were performed for both the stainless steel and carbon steel pipe experiments. In each analysis, loading was increased monotonically until crack initiation and then the maximum stress was predicted. For these analyses the quasi-static stress-strain curves are used with the quasi-static J-R curve. Quasi-static properties were used to reflect the more normal situation in which dynamic data are not available.

Results of Comparisons of SC.TNP Predictions to Pipe Fracture Experiments. The results of the SC.TNP predictions of crack initiation stresses and maximum stresses for Experiments 1.1-5 (stainless steel, inertial, surface-crack experiment) and 1.1-7 (carbon steel, dynamic, monotonic, surface-crack experiment) are presented in Table 4.6 and Figure 4.13. Predictions of crack initiation

Table 4.6. SC.TNP predictions of crack initiation stresses and maximum stresses for Experiments 1.1-5, 1.1-7, and 1.1-9

Experiment No.	Experimental Stress, MPa ^(a)	Predicted Stress, MPa ^(a)	$\frac{\text{Experimental Stress}^{(a)}}{\text{Predicted Stress}^{(a)}}$
Crack Initiation Predictions			
1.1-5	201.1	208.2	0.97
1.1-7	360.0	309.8	1.16
1.1-9	265.0	289.3	0.92
Maximum Stress Predictions			
1.1-5	258.8	208.9	1.24
1.1-7	367.8	320.0	1.15

(a) Includes axial stress contribution due to internal pipe pressure.

Figure 4.13. Bar chart showing the ratio of the experimental stresses to the SC.TNP predicted stresses for a number of Subtask 1.1 surface-cracked-pipe experiments

II.1-6/91-F4.13

stresses only are also presented for Experiment 1.1-9. No predictions of maximum stress are presented for this experiment. Soon after the crack initiated the depth of the flaw was beyond the range where valid h_1 functions were available. SC.TNP predictions of neither crack initiation nor maximum stress are available for the remaining two Subtask 1.1 surface-cracked-pipe experiments (1.1-6, and 1.1-8). The initial flaw was so deep that valid h_1 functions were not available. The h_1 functions are only valid to d/t ratios of 0.75. They are very nonlinear in this region, so linear extrapolation is not appropriate. Generation of h_1 functions for deeper cracks is required to solve this problem.

Predictions for Stainless Steel Inertial Pipe Experiment (1.1-5). For the analysis of this experiment, the initial flaw size used was a depth of approximately 64 percent of the thickness and a length of half of the pipe circumference. For the maximum-load predictions, the J_D -R curve was used. Since the amount the crack extended before maximum load was reached is small for these surface-cracked-pipe experiments, the choice of J_D -R or J_M -R curves for the maximum-load predictions is immaterial.

The stress at crack initiation was predicted using only the static material properties. These results are given in Table 4.6 and Figure 4.13. The experimental stress at crack initiation was 97 percent of the predicted stress.

The maximum stress was also predicted using only the static material properties. These results are given in Table 4.6 and Figure 4.13. The experimental maximum stress was 124 percent of the predicted stress.

Predictions for Carbon Steel, Dynamic Monotonic Experiment (1.1-7). Quasi-static material property data were used to predict the stress at crack initiation and the maximum stress for Experiment 1.1-7. From Table 4.6 and Figure 4.13 it can be seen that the analysis underpredicted the experimental stress at crack initiation by approximately 16 percent and underpredicted the maximum experimental stress by approximately 15 percent.

Predictions of Crack Initiation Stresses for Carbon Steel, Quasi-Static Monotonic Experiment 1.1-9. Quasi-static material property data were used to predict the stress at crack initiation for Experiment 1.1-9. From Table 4.6 and Figure 4.13, it can be seen that the analysis overpredicted the experimental stress at crack initiation by approximately 8 percent.

4.1.4 R6 Revision 3 Option 1 Procedures

The R6 method is a fracture prevention design criterion developed by CEGB, Ref. 4.16. The most recent refinement is referred to as R6 Revision 3. Within this version there are three options. The first option is the simplest, and does not require the use of the material stress-strain curve for input. The second option requires the use of the material stress-strain curve, and hence is slightly more complicated. The third option allows the use of any appropriate elastic-plastic fracture mechanics analysis, e.g., the GE/EPRI method, to create the appropriate failure assessment curve. In this last option, the R6 method essentially would give the same predictions as the J-estimation scheme analysis given in Section 4.1.3 of this report. For the R6 analysis in the current section, only predictions using Option 1 will be compared with the experimental results.

4.1.4.1 The Computational Procedure

In the Option 1 procedure, a fixed failure assessment curve is used for any material, crack or structural geometry. The failure assessment curve has a load or stress ratio (L_r or S_r) along the x-axis, and a toughness ratio, K_r , using either K or J fracture parameters along the y-axis, see Figure 4.14.

The load ratio term, L_r , is the applied load divided by the limit load. The limit load is the Net-Section-Collapse predicted load for circumferentially cracked pipe. For pipe under pressure and bending, such as the IPIRG Subtask 1.1 experiments, this ratio could be interpreted as being the total applied stress (bending plus tension from the pressure) divided by the total stress predicted from the Net-Section-Collapse analysis,

$$L_r = (\sigma_b + \sigma_a) / (\sigma_{bNSC} + \sigma_a). \quad (4.10)$$

**Figure 4.14. R6 Option 1 failure analysis diagram
F1.3-10/90-F4.8**

Alternatively, L_r could be defined as the ratio of the applied bending stress to the Net-Section-Collapse predicted bending stress,

$$L_r = \sigma_b / \sigma_{bNSC} \quad (4.11)$$

Note that in the R6 method, the yield strength, not the flow stress, is used in the limit-load equation. The ratio of the flow stress to the yield strength can be used as the cut-off point along the L_r axis to be consistent with the Net-Section-Collapse analysis, see Figure 4.14.

The toughness ratio, K_r , is the ratio of the applied linear elastic stress intensity factor, K_a , to the toughness of the material, $K_r(a)$, where $K_r(a)$ is a function of crack growth from the J-R curve of the material.

$$K_r = K_a / K_r(a), \quad (4.12)$$

where

$$K_I(a) = (JE)^{0.5} . \quad (4.13)$$

Here J is the value corresponding to Δa from the J - R curve of the material and E is the elastic modulus.

The applied linear elastic stress intensity factor, K_a , can be separated into membrane and bending components,

$$K_a = K_{lm} + K_{lb} . \quad (4.14)$$

The terms K_{lm} and K_{lb} are defined in this report by the solutions given in the ASME Section XI ferritic pipe flaw evaluation procedure, Article IWB-3650, Ref. 4.5.

4.1.4.2 Results of Comparisons of R6 Predictions with Experimental Results

For the Subtask 1.1 experiments, moments and stresses using the R6 Revision 3 Option 1 method were calculated for both crack initiation and maximum load (see Table 4.7). Instead of comparing these predictions with experimental stresses, as was done for the predictive analysis (NSC, DPZP, and J -estimation schemes) previously discussed, the R6 predictions are compared with stresses from an elastic stress analysis of the experiments. Elastic calculations are more in line with the R6 method in that the R6 analysis uses elastically calculated stresses for its assessment of a defect. For the elastic stress analyses of these experiments, the actual displacement-time history from the experiment was applied to the elastic analysis. The resultant elastically calculated moments at crack initiation and maximum moment were converted to stress and added to the axial tension stress due to internal pipe pressure. These are the stresses reported in Table 4.7.

The results for Table 4.7 are presented in graphical form in Figures 4.15 and 4.16. Figure 4.15 shows the comparisons of the elastically calculated stresses to the predicted stresses for crack initiation. Figure 4.16 is for the maximum stress. In the maximum stress calculations, the crack growth was incremented causing $K_I(a)$ to increase, K_a to increase, and σ_{bNSC} to decrease. In these calculations, the quasi-static J_D - R and J_M - R curves at the pipe test temperature were used. (The choice of J_D or J_M made no difference for the crack initiation predictions and little difference for the surface crack maximum stress predictions.) The yield strength used is the average of values from quasi-static tests at the pipe test temperature. The effect of dynamic material property data was not considered. The cutoff point on the L_r axis is the ratio of the flow stress to the yield strength, where the flow stress is the average of the quasi-static yield and ultimate strengths.

Table 4.7. Comparison of R6 Revision 3 Option 1 predictions with elastic stress analysis results for Subtask 1.1 experiments

		R6 Predicted Stress, MPa ^(a)		Elastic Stress ^(a) Predicted Stress ^(a)	
Experiment No.	Stress From Elastic Stress Analysis, MPa ^(b)	Using J _D -R Curve	Using J _M -R Curve	Using J _D -R Curve	Using J _M -R Curve
Crack Initiation Predictions	Crack Initiation Predictions				
1.1-2	117.5	110.5	110.5	1.06	1.06
1.1-3	150.3	66.7	66.7	2.25	2.25
1.1-5	206.9	149.5	149.5	1.38	1.38
1.1-6	N.D.	147.2	147.2	—	—
1.1-7	370.4	262.9	262.9	1.41	1.41
1.1-9	277.2	239.3	239.3	1.16	1.16
Maximum Stress Predictions	Maximum Stress Predictions				
1.1-2	228.1	134.8	137.3	1.69	1.66
1.1-3	261.8	66.7	68.1	3.93	3.84
1.1-5	359.4	149.5	149.5	2.40	2.40
1.1-6	411.6	147.6	147.6	2.79	2.79
1.1-7	386.9	272.1	272.1	1.42	1.42
1.1-9	316.7	248.5	248.5	1.27	1.27

(a) Axial plus bending stress.

(b) Using experimental time history in elastic uncracked pipe analysis up to the time of the experimental maximum load.

Figure 4.15. Bar chart showing the ratio of the calculated elastic stresses at crack initiation to the R6 Revision 3 Option 1 predictions of crack initiation
 11.1-6/91-F4.15

Figure 4.16. Bar chart showing the ratio of the calculated elastic stress at maximum stress to the R6 Revision 3 Option 1 predictions of maximum stress
 11.1-6/01-F4.16

Examining the data in Table 4.7 and Figures 4.15 and 4.16, it is evident that the R6 Revision 3 Option 1 approach underpredicted the elastically calculated stresses in all cases. For the four inertial experiments, the analysis significantly underpredicted the elastically calculated values. The most significant underprediction (approximately 300 percent) is the maximum stress prediction for the stainless steel through-wall crack experiment (1.1-3). This is not surprising since the higher toughness stainless steel specimen will experience much more nonlinearities before maximum stress is attained than will the lower toughness carbon steel specimens. These nonlinearities will manifest themselves as higher margins for the R6 analysis when compared with elastically calculated stresses. The general trend is that the margins are higher for the maximum stress case than the corresponding crack initiation case. This is reflective of the increased nonlinearities associated with the higher loads at maximum stress when compared with the loads at crack initiation. For the two displacement-controlled experiments, the degree of underprediction is much less. The R6 Revision 3 Option 1 method underpredicted the elastic maximum stresses by 27 to 42 percent for these two experiments.

4.1.5 ASME Section XI Flaw Evaluation Procedures

Within Section XI of the ASME Boiler and Pressure Vessel Code, flaw evaluation procedures for cracks in austenitic and ferritic piping have been developed. The austenitic pipe flaw evaluation procedures are in Article IWB-3640. These were developed first in 1985, and have been modified to include procedures for evaluation of cracks in lower toughness welds, Ref. 4.4.

The ferritic pipe flaw evaluation procedures are given in the ASME Code as Code Case N-463 as well as Article IWB-3650 in Section XI, and Appendix H of Section XI (Ref. 4.5).

An important aspect not considered in this assessment of the ASME criteria is that, like the R6 analysis, the ASME flaw evaluation criteria uses the plant elastic uncracked pipe stress analysis. The analysis conducted here compared the ASME predicted stresses directly to the experimental stresses. This was done to be consistent with past verification analyses of the flaw acceptance criteria. Using an elastic uncracked stress analysis with the experimental displacements at failure will be examined in the IPIRG final report.

4.1.5.1 Austenitic Pipe Fracture Experiments

The ASME flaw evaluation procedure for cracks in austenitic stainless steel base metal are essentially the same as the Net-Section-Collapse analysis procedure, discussed in Section 4.1.1. The major differences between these two analysis procedures are:

- the flow stress is defined as being equal to $3S_m$ in the ASME code rather than the average of yield and ultimate,
- for low toughness flux welds, i.e., SAW and SMAW, the ASME procedure uses a Z-factor to account for the toughness of the weld being much lower than that for the base metal, and
- for combined tension and bending, the Net-Section-Collapse analysis and the ASME Code β terms differ slightly.

For the ASME analysis

$$\beta = 0.5 (\pi - \theta a/t - \pi P_m / \sigma_f) \quad (4.15a)$$

where $\sigma_f = 3S_m$,

and for the Net-Section-Collapse analysis,

$$\beta = 0.5 [\pi - \theta a/t - \frac{\pi R_L^2 p}{2R_m t \sigma_f}] \quad (4.15b)$$

Table 4.8a presents the maximum experimental stresses and the stresses predicted by the ASME Section XI IWB-3640 analysis procedure for the Subtask 1.1 stainless steel pipe experiments. The exact pipe and crack dimensions were used. These results do not include either the ASME Code safety factors of 2.78 for normal and test conditions, or the 1.39 safety factor for emergency and faulted loads. Note that the experimental stresses in Table 4.8 are slightly higher than those reported previously in Tables 4.2 through 4.6. For the predictive analysis methods such as Net-Section-Collapse, Dimensionless Plastic-Zone Parameter, and the J-estimation schemes, a more exact thick-wall formulation for the axial tension stress due to pressure was used. For the ASME Code comparisons in Table 4.8, the axial tension stress is calculated using a thin-wall formulation as per ASME Section III NB3652,

$$\sigma_a = \frac{pD_o}{4t} \quad (4.16)$$

where p = pipe pressure,
 D_o = outside diameter, and
 t = wall thickness.

In Table 4.8a, the calculations of the predicted failure stress were made using two different definitions of S_m (i.e., flow stress) for two distinctly different reasons.

Table 4.8a. Comparison of experimental results with IWB-3640 predictions for the two Subtask 1.1 stainless steel pipe experiments

		Predicted Stress, MPa ^(a)		<u>Experimental Stress^(a)</u> Predicted Stress ^(a)	
Experiment No.	Experimental Stress, MPa ^(a)	S _m (Code)	S _m (Actual)	S _m (Code)	S _m (Actual)
1.1-3	164.8	156.3	168.2	1.05	0.98
1.1-5	269.9	245.5	263.1	1.10	1.03

(a) Included axial stress contribution due to internal pressure.

Table 4.8b. Comparison of experimental results with IWB-3650 predictions for the five Subtask 1.1 carbon steel pipe experiments

		Predicted Stress, MPa ^(a)		<u>Experimental Stress^(a)</u> Predicted Stress ^(a)	
Experiment No.	Experimental Stress, MPa ^(a)	S _m (Code)	S _m (Actual)	S _m (Code)	S _m (Actual)
1.1-2	204.1	105.5	152.2	1.93	1.34
1.1-6	244.9	106.6	155.4	2.30	1.58
1.1-7	379.0	178.6	250.9	2.12	1.51
1.1-8	375.4	149.8	212.1	2.51	1.77
1.1-9	302.8	162.2	228.6	1.87	1.32

(a) Includes axial stress contribution due to internal pressure.

The first definition of flow stress uses S_m defined from Tables I-1.1 and I-1.2 of Section III of the ASME Code. In Table 4.8a, these predictions fall under the heading of $S_m(\text{Code})$. They provide a direct comparison of the experiments to the ASME Code procedures.

The second definition of the flow stress was created to evaluate the technical basis of the Code. It is an attempt to analyze the pipe experiments as if the pipes evaluated had strength properties consistent with the design strength values defined in Section III of the ASME Code. In this program no attempt was made to procure pipes with lower bound strength properties. To account for the fact that the pipes evaluated had higher strengths than what might be expected on the basis of the Section II S_m values, an S_m value was calculated using the actual (quasi-static) properties of the pipe tested. In this analysis, $3S_m(\text{Actual})$ was used as the flow stress. The term $S_m(\text{Actual})$ was defined in the spirit of ASME Section III Article 2110 as the minimum of:

- (1) one-third of the actual room temperature ultimate tensile strength (for both ferritic and austenitic pipes),
- (2) one-third of the actual ultimate strength at the pipe test temperature (for both ferritic and austenitic pipes),
- (3) two-thirds of the actual yield strength at room temperature (for both ferritic and austenitic pipes), or
- (4) two-thirds of the actual yield strength at the pipe test temperature (for ferritic pipes only), or 90 percent of the actual yield strength at the pipe test temperature (for austenitic pipes only).

The results in Table 4.8a for the two Subtask 1.1 stainless steel pipe experiments are shown graphically in Figure 4.17. From Table 4.8a and Figure 4.17 it can be seen that the ASME Section XI IWB-3640 procedures accurately predicted the experimental results from these experiments. The Code predictions agreed within 10 percent of the experimental results. In addition, with the exception of the stainless steel through-wall-cracked-pipe experiment (1.1-3), when actual properties were used to define the flow stress, the IWB-3640 procedures underpredicted the experimental results. Even for that case, the degree of overprediction was small, i.e., 2 percent. It is also of note from Table 4.8a and Figure 4.17 that the effect of using actual properties to define an S_m value was minor, since the strength of this particular heat of stainless steel was close to the Section II S_m value at 288 C (550 F).

4.1.5.2 Ferritic Pipe Fracture Experiments

The ASME ferritic pipe flaw evaluation procedure is similar to the austenitic procedure. However, the ferritic pipe flaw evaluation procedure is slightly more complicated than the austenitic procedure since it must account for failure being below Net-Section-Collapse stress for a flaw in the base metal. The major differences between the ASME Code and the Net-Section-Collapse analysis are:

- the flow stress used for ferritic pipe is $2.4S_m$ in the ASME Code,

Figure 4.17. Bar chart showing the ratio of the maximum experimental stress to IWB-3640 predicted stress for the two Subtask 1.1 stainless steel experiments using two definitions of flow stress

II.1-6/91-F.17

Figure 4.18. Bar chart showing the ratio of the maximum experimental stress to the IWB-3650 predicted stress for the Subtask 1.1 carbon steel experiments using two definitions of flow stress

II.1-6/91-F4.18

- there is a screening criterion used to assess if limit-load, elastic-plastic fracture, or linear-elastic fracture analysis should be used in the ASME Code procedure, and
- there is a simple stress multiplier used for the elastic-plastic analysis method to account for the lower stresses.

The stress multiplier, Z , used for a crack in the base metal (Material Category 1 in Ref. 4.5) is

$$Z = 1.20 [1 + 0.021A (NPS - 4)], \quad (4.17)$$

where

$$A = [0.125(R_m/t) - 0.25]^{0.25} \text{ for } 5 \leq R_m/t \leq 10, \text{ or}$$

$$A = [0.4(R_m/t) - 3.0]^{0.25} \text{ for } 10 < R_m/t \leq 20,$$

$$R_m = \text{mean pipe radius,}$$

$$t = \text{pipe thickness, and}$$

$$NPS = \text{nominal pipe size, inches.}$$

For the pipe in this effort, R_m/t is 5.5, hence A is 0.813, and Z is 1.24. (Note, the NPS [nominal pipe size] for the experiments in this report is 6 inches.)

The predicted ASME failure stresses for the Subtask 1.1 carbon steel experiments, without the ASME safety factors, are given in Table 4.8b. The data in Table 4.8b are presented in graphical form in Figure 4.18. As is the case of the stainless steel experiments, two different definitions of S_m were used in defining the flow stress; i.e., $S_m(\text{Code})$ and $S_m(\text{Actual})$. In examining Table 4.8b and Figure 4.18, it can be seen that for the Subtask 1.1 ferritic pipe experiments, the ASME Code approach significantly underpredicted the experimental results. The ratio of the experimental stresses to the IWB-3650 predicted stresses ranged from 1.87 to 2.51 when Code values of S_m were used to define the flow stress. When actual properties were used to define the flow stress, the ratios of the experimental stresses to the predicted stresses decreased significantly, i.e., 30 percent. This reflects the fact that this particular heat of carbon steel had significantly higher strength than the Code values.

4.2 J-R Curves from Specimens and Pipe Experiments

The η -factor analysis was used to determine the value of J at crack initiation (J_i) for a number of the Subtask 1.1 surface-cracked-pipe experiments and the full J-R curve for the stainless steel through-wall-cracked pipe experiment (1.1-3). Experiment 1.1-3 was the only experiment for which through-wall-crack-growth data were available. The η -factor analysis is a general analytical procedure in which fracture resistance is calculated using the experimental load-displacement (or moment-rotation) and crack-growth data from the pipe experiment. It is used in lieu of three-dimensional finite-element analyses. The analysis method was first developed by Zahoor (Ref. 4.2) for circumferential through-wall-cracked pipe. Since Zahoor's initial formulation, a number of improvements have been

made (Ref. 4.19). Pan extended the method to consider the case of a circumferential surface crack in a pipe (Ref. 4.20).

The η -factor analysis determines J by dividing it into its elastic and plastic contributions.

$$J = J_e + J_p \quad (4.18)$$

The elastic contribution to J (J_e) is obtained from handbook solutions (i.e., Ref. 4.17). The plastic contribution to J (J_p) is derived by integrating the experimental load-displacement, or moment-rotation, relationship. The result of that integration is multiplied by a constant η ,

$$J_p = \eta \int_0^{\phi_1} M d\phi_p^c \quad (4.19)$$

where ϕ_p^c is the plastic component of the rotation due to the crack.

The factor η is geometry dependent. Specific forms for through-wall cracks and surface cracks are found in References 4.19 and 4.20, respectively. The above formulation is for a stationary crack, i.e., up to crack initiation. Once crack growth starts, η varies with crack size and a crack-growth correction term is needed.

The η -factor solution is for a pipe in pure bending. However, the Subtask 1.1 experiments involve combined pressure and bending. To account for the pressure in the η -factor J_p solution, an equivalent moment, M_{eq} , due to the pressure was calculated. This was accomplished using the Net-Section-Collapse equations for a crack in a pipe and solving for the failure moment for the cases of pure bending and combined pressure and bending. The difference between the two solutions is taken as the equivalent moment due to the internal pressure, M_{eq} . This moment was added to the experimental moments in the moment-rotation relationship to give a moment equivalent to the moment that would exist if the pipe were subjected to pure bending.

The upper envelope of the load-displacement curves was used for analyzing the cyclically loaded Subtask 1.1 pipe experiments. Technically, this is not strictly correct, but it is the simplest approach, from an engineering viewpoint, and is similar to ASTM unloading compliance testing procedures, but with larger unloadings.

Note that for the surface-cracked-pipe experiments, the internal surface-cracked pipes were the only ones analyzed, since an η -factor solution for external circumferential surface-cracked pipe was not readily available.

4.2.1 J_i Values from Subtask 1.1 Pipe Experiments

Values of J at crack initiation (J_i) were calculated using the η -factor method for one of the Subtask 1.1 through-wall-cracked-pipe experiments and the three internal surface-cracked-pipe experiments for which crack initiation data were available. These J_i values were calculated using a PC code for calculating η factors initially developed in the Degraded Piping Program, and subsequently refined in the IPIRG Program. The results of those calculations, along with the C(T) specimen J_i values for the appropriate materials, are shown in Table 4.9.

Table 4-9. Comparison of J_i values for η -factor method with J_i values from C(T) specimens for Subtask 1.1 experiments

Experiment No.	Pipe Test Loading	Material	Flaw Type	J_i , kJ/m ²		
				η -Factor Analysis	QS-Monotonic C(T) Specimen	Dynamic Monotonic C(T) Specimen
1.1-3	Inertial	TP304	TWC	451	594	675
1.1-5	Inertial	TP304	SC	155	594	675
1.1-7	Dyn-Mono	A106B	SC	201	70	59
1.1-9	QS-Mono	A106B	SC	92	70	59

Each of these experiments has different sensitivities to geometry, loading history, or material sensitivity to loading rate effects. Thus, it is not easy to say that the pipe J values are always higher or lower than standard C(T) specimen J_i values. With the experience gained from the IPIRG Subtask 1.2 TWC pipe cyclic pipe tests, the significance of the J_i values from each of these test results is discussed below.

4.2.1.1 Experiment 1.1-3; Inertial TWC TP304 Pipe Experiment

In Experiment 1.1-3, the loading was constant pressure plus a dead-weight bending load, and an inertial bending load to failure. The pressure and dead-weight components supply a quasi-static load contribution while the inertial loading supplies a dynamic cyclic component. Based on the material property test data in Section 2 of this report, and the pipe test data in IPIRG Subtask 1.2, the dynamic loading should result in a slight increase in the fracture toughness of the stainless steel pipe, but the cyclic loading could reduce it.

From the IPIRG Subtask 1.2 results, the parameters used to assess if cyclic loading is detrimental to a material's toughness are the load-ratio (R) and the cyclic plastic displacement. The load-ratio is the minimum stress divided by the maximum stress. From the IPIRG Subtask 1.2 pipe experiments, it was found that a load-ratio greater than zero was not detrimental, and a load-ratio of -1 was detrimental to the toughness. From the Subtask 1.2 work, we do not know how the toughness changes between load-ratios of 0 to -1. In Experiment 1.1-3, the load-ratio based on the total longitudinal stress at crack initiation was 0.23, which from the Subtask 1.2 TWC pipe experiments suggests that there should be no cyclic degradation of the toughness.

From Table 4.9, the pipe J_I -factor J_I value was 76 percent of the C(T) specimen J_I value. This difference could be potentially due to the accuracy to which crack initiation can be determined in a tough material like stainless steel, and is not thought to be significant.

4.2.1.2 Experiment 1.1-5; Inertial Surface-Cracked TP304 Pipe Experiment

As was the case for the TWC inertial experiment, there are quasi-static and dynamic contributions to the load-ratio (R) for this inertially loaded pipe experiment. The load-ratio based on the total longitudinal stress at crack initiation was -0.27.

Currently there are no cyclic surface-cracked pipe tests like the IPIRG Subtask 1.2 TWC pipe tests to use to see if the trends from the cyclic TWC pipe tests will be similar for surface-cracked pipe experiments. From what we know now, the load-ratio of -0.27 could be sufficient to cause cyclic degradation of the toughness based on the TWC pipe experiments.

The Experiment 1.1-5 pipe J_I value was found to be much lower than the C(T) specimen J_I value. The pipe J_I value was 26 percent of the C(T) specimen J_I , which is a significant difference that is beyond the error that could be made in determining the crack initiation point. The only other stainless steel surface-cracked pipe experiment analyzed under similar dynamic/cyclic loading was Experiment 1.3-3 in the IPIRG pipe system experiments, Ref. 4.21. In that case, it was also found that the surface-cracked pipe under cyclic dynamic loading ($R = -0.34$) had a toughness of 29 percent of the quasi-static C(T) specimen toughness, which is in remarkable agreement with this test result. For reference, it should also be noted that in the TWC Subtask 1.2 stainless steel pipe experiments, the J_I value for $R = -1$ was about 25 percent of the monotonic value.

The implication from this analysis is either: (1) there is a sharp transition of the change in toughness from $R = 0$ to $R = -0.27$, or (2) surface-cracked pipes are more susceptible to cyclic toughness degradation than TWC pipe.

Another factor that will be discussed below, is that there is also evidence that a surface-cracked pipe should have a higher toughness than a TWC pipe, possibly due to the loss of constraint through the thickness. This was empirically found from the Dimensionless Plastic-Zone Screening criterion developed at Battelle during the Degraded Piping Program. In that work, surface-cracked pipe seemed to have a toughness of a factor of 4 higher than TWC pipe, Ref. 4.9. Hence, whereas we expected the toughness to be higher for this surface-cracked-pipe experiment than for the C(T) specimen by a factor of 4, it was actually lower by a factor of 4, possibly due to cyclic loading.

4.2.1.3 Experiment 1.1-7; Dynamic Monotonic Bending with Constant Internal Pressure of a Surface-Cracked A106 Grade B Pipe

In this experiment, several variables changed from the two experiments previously discussed. Here there is no cyclic loading, and also this A106 Grade B pipe is slightly more sensitive to loading rates than the stainless steel pipe was, i.e., the C(T) specimen J_I decreased by approximately 15 percent at the higher loading.

Most of the stress is dynamically applied because the pressure causes a static longitudinal stress of less than 10 percent of the total longitudinal stress. From Table 4.9, the ratio of the pipe to the quasi-static C(T) specimen J_i was 2.87, which is a significant difference. From the previous discussion of Experiment 1.1-5, it would not have been surprising to see the surface-cracked pipe J_i to be a factor of 4 higher. The dynamic loading should have caused the factor of 4 to be slightly lower for this material, which it was.

4.2.1.4 Experiment 1.1-9; Quasi-static Monotonic Bending with Constant Internal Pressure of a Surface-Cracked A106 Grade B Pipe

In this experiment, there are very few variables that should affect the toughness calculated from the pipe test relative to the C(T) specimen data. The only difference is one of geometry. It was discussed previously that a surface crack in a pipe could have an apparent toughness 4 times higher than a through-wall crack in the same pipe.

From Table 4.9 it can be seen that the pipe toughness in this experiment is only 1.28 times higher than the toughness in the quasi-static C(T) specimen, and is 45 percent of the Experiment 1.1-7 value. This experiment clearly does not follow the trends expected. Based on the static versus dynamic C(T) test results, the J_i from this experiment should be higher than the 1.1-7 value by about 15 percent, not lower by a factor of 2.23 (i.e., $1/0.45$).

A direct comparison of the loads at failure for Experiments 1.1-7 and 1.1-9 is not appropriate because of differences in the surface-crack depth (d/t of 0.647 for Experiment 1.1-7 and d/t of 0.72 for Experiment 1.1-9). Accounting for the differences in the crack depth by the different analyses previously discussed in this section of the report (i.e., using the Net-Section-Collapse analysis, Dimensionless Plastic-Zone Screening criterion, R6 Option 1 analysis, and the SC.TNP J-estimation scheme) shows that Experiment 1.1-9 did have a lower load-carrying capacity than Experiment 1.1-7, indicating that the toughness was really lower in Experiment 1.1-9 than in Experiment 1.1-7.

Examining the experimental data further, it can be seen in Figures 3.39 and 3.44 that crack initiation appears to be reasonably well-defined for both experiments from the d-c electric potential versus crack opening displacement data. From these figures, it can be seen that the ratio of the electric potential at crack initiation to the electric potential at the start of the test, was the same in both experiments, indicating that the initiation points were consistently picked. Hence, the choice of the initiation loads in these two experiments do not seem to be the source of the discrepancy.

One other factor that should also be mentioned for these A106 Grade B pipe experiments, is that from the work in IPIRG Subtask 1.2 experiments using the same piece of pipe, it was found that this particular A106 pipe has much more scatter in its properties than other nuclear pipes examined in past Battelle programs. Significant helical cold-working patterns were found in the pipe when machining the pipe to a constant thickness for these experiments. Also as shown in Section 2 of this report, the ultimate strength varied by almost 20 percent from specimen to specimen. Additionally, triplicate TWC dynamic monotonic pipe experiments in Subtask 1.2 showed significant differences in toughness, up to a factor of 2.2 on this same pipe length. Hence, it may be that refined conclusions drawn from experiments on this particular pipe material are clouded by the variation in properties.

4.2.2 J-R Curve from Subtask 1.1 Stainless Steel Through-Wall-Cracked-Pipe Experiment (1.1-3)

The stainless steel through-wall-cracked-pipe experiment (1.1-3) was the one Subtask 1.1 experiment for which crack-growth data were available. As such, it is the only experiment for which it is possible to calculate a full J-R curve using the η -factor method. The J-R curve calculated using the η -factor approach for this experiment is shown in Figure 4.19. For comparison purposes, the J-R curves from the companion quasi-static pipe experiment (4131-5) and one of the quasi-static C(T) specimens for this material (A23) are also included in Figure 4.19. In examining Figure 4.19, it can be seen that the J-R curve for the inertial experiment is about 60 percent of the J-R curve from the companion quasi-static pipe experiment and slightly lower than the J-R curve from the quasi-static C(T) specimen test. The lower J-R curve from the inertial pipe test could be due to either cyclic or dynamic loading effects, or variations in the toughness. However, the C(T) tests in Section 2 and the pipe tests in IPIRG Subtask 1.2 suggest that TP304 stainless steel J-R curves are not sensitive to loading at these strain rates. From the IPIRG Subtask 1.2 experiments, the reduction of toughness due to cyclic loading with an $R > 0$ is also expected to be minimal.

Figure 4.19. J-R curve using the η -factor analysis for Experiment 1.1-3. (For comparison, the J-R curves for the companion quasi-static monotonic pipe experiment (4131-5) and a quasi-static C(T) specimen are also included.)

11.1-6/91-F4.19

4.3 Dynamic Analyses

The two Subtask 1.1 through-wall-cracked, inertially-loaded, pipe-fracture experiments were dynamically analyzed. This was done using the ANSYS computer code with beam elements and a nonlinear spring at the crack location to represent the cracked-pipe moment-rotation response. This procedure was discussed in the pretest design analysis section of this report.

4.3.1 Dynamic Analysis of Experiment 1.1-3

Pretest design calculations were conducted for Experiment 1.1-3 prior to conducting the actual experiment. The nonlinear-spring element concept was used in the ANSYS program as described in Section 3 of this report. The moment-rotation response data from a quasi-static experiment on the same pipe with the same crack size provided the calibration for the nonlinear spring element used in the pretest design analysis. The most significant difference between the analysis and the experiment was in the frequency of the forcing function. The analysis was done using 3 Hz for the input forcing function while the experiment was conducted at a frequency of 3.5 Hz. The higher frequency was chosen for the experiment so that the increased dynamic amplification (from being nearer to the first natural frequency) would provide a greater margin for the successful conduct of the experiments.

Using the forcing function $\delta = (\Psi t) \sin(\omega t)$, where $\omega = 18.85$ radians/second (3 Hz) and $\Psi = 25.4$ mm/sec (1.0 inch/sec), the analysis was conducted until the maximum moment was calculated in the test section. Figures 4.20a, 4.21a, and 4.22a show the results of the analysis for moment versus time, inertial mass displacement versus time, and moment versus rotation response, respectively, for this stainless steel, through-wall-cracked, inertial experiment. Experimental data are shown in the companion plots, Figures 4.20b, 4.21b, and 4.22b. Table 4.10 compares the predicted-to-experimental maximum moments, time to maximum moment, and the rotation of the cracked pipe at the maximum moment.

The excellent agreement in moments and rotations reflected in Table 4.10 for this stainless steel experiment should not be surprising in that the moment-rotation response used in the analysis is based on actual experimental quasi-static data and this material is not sensitive to loading rate effects. In any event, considering the differences in the actual forcing function used in the experiment versus that used in the pretest analysis, the predicted dynamic results are extremely good for such a simple model.

4.3.2 Dynamic Analysis of Experiment 1.1-2

The analysis of this experiment was conducted using techniques similar to those used for the analysis of Experiment 1.1-3. The specimen was carbon steel with a TWC and contained a bladder. The displacement-time history used as an input to the ANSYS finite-element model was the function $\delta(t) = (\Psi t) \sin \omega t$, where $\omega = 22$ radians/second (3.5 Hz) and $\Psi = 2.0$ mm/s (0.08 inch/sec). The analysis was conducted until maximum moment was observed in the test section.

Contrary to the analysis of Experiment 1.1-3, the moment-rotation response for this analysis did not come from a previously conducted pipe experiment. The data used to analyze this carbon steel experiment was generated using the C(T) specimen dynamic J-R curve and the dynamic tensile

(a) Moment-time from analysis

(b) Moment-time from experiment

Figure 4.20. Comparison of experiment with analysis for moment versus time plots of Experiment 1.1-3 (TP304 TWC). [The analysis was valid only up to maximum moment.]

I.1.2-6/91-F4.20

(a) Inertial mass displacement from analysis

(b) Inertial mass displacements from experiment

Figure 4.21. Comparison of experiment with analysis for displacements of the inertial masses for Experiment 1.1-3 (TP304 TWC)
11.2-6/91/F4.21

(a) Moment-rotation from analysis

(b) Moment-rotation from experiment

Figure 4.22. Comparison of experiment with analysis for moment versus rotation for Experiment 1.1-3 (TP304 TWC)
11.2-6/91-F4.22

Table 4.10. Comparison of dynamic predictions using ANSYS code and nonlinear spring (cracked pipe) element with through-wall-cracked-pipe experimental results

Stainless Steel Experiment 1.1-3		
	Analysis	Experiment
Maximum Moment, kN-m	28.7	30.6
Time to Maximum Moment, sec	1.325	1.28
Rotation at Maximum Moment, radian	0.10	0.12
Carbon Steel Experiment 1.1-2		
	Analysis	Experiment
Maximum Moment, kN-m	42.9	38.9
Time to Maximum Moment, sec	9.6	8.6
Rotation at Maximum Moment, radian	0.008	.012

Table 4.11. Moments calculated by an elastic stress analysis compared with measured moments for the IPIRG Subtask 1.1 pipe-fracture experiments

Experiment No.	Material	Flaw Geometry	$\frac{M_{elastic}}{M_{test}}$
1.1-2	A106 B	Through-wall crack	1.22
1.1-3	TP304	Through-wall crack	1.92
1.1-5	TP304	External surface crack	1.45
1.1-6	A106 B	Internal surface crack	1.89
1.1-7	A106 B	Internal surface crack	1.06
1.1-9	A106 B	Internal surface crack	1.10

stress-strain curve. Battelle's NRCPIPE code using the LBB.ENG2 analysis method was used to predict the moment-rotation relationship for this experiment.

Using the forcing function described above with the NRCPIPE calculations to calibrate the nonlinear spring element, ANSYS was used to generate moment, force, and displacement data which were compared to the experimentally obtained data. Figures 4.23, 4.24, and 4.25 show the moment versus time, inertial mass displacement versus time, and rotation versus time plots, respectively. Table 4.10 compares experimental and predicted results of the maximum moment, time to maximum moment, and the rotation of the cracked pipe at the maximum moment for this carbon steel, through-wall-cracked, inertial experiment. Considering the fact that in lieu of experimental pipe fracture data, a J-estimation scheme was used to establish the moment versus rotation response for the nonlinear spring element in the analysis, these predictions show excellent agreement with the experimental results.

4.3.3 Elastic Stress Analysis

Since both the R6 and ASME flaw evaluation procedures use linear elastic uncracked pipe stress analyses, it was desired to compare the linear elastic stresses with the experimental stresses separately from the fracture analyses. This will assess the contribution of the stress analysis differences from the R6 and ASME flaw evaluation procedures. ANSYS calculations were performed for each of the flawed pipe tests using, as input, the actual load-point-displacement versus time history obtained during the test. The analysis was linear elastic without a crack element. The moments obtained were then compared with the maximum moments obtained during the test.

Table 4.11 gives the ratio of the elastic moment to the measured moment for each of the Subtask 1.1 pipe fracture experiments.

(a) Moment time from analysis

Figure 4.23. Comparison of experiment with analysis for moment versus time plots of Experiment 1.1-2 (A106B TWC)
IWS-11/88-I6&I11-ROR

(a) Inertial mass displacements from analysis

(b) Inertial mass displacements from experiment

Figure 4.24. Comparison of experiment with analysis for displacements of the inertial masses for Experiment 1.1-2 (A106B TWC)
II.1-10/89-F4.15R

(a) Rotation time from analysis

(b) Rotation time from experiment

Figure 4.25. Comparison of experiment with analysis for rotations at crack plane for Experiment 1.1-2 (A106B TWC)
 11.1-10/89-F4.16

4.4 References

- 4.1 Kanninen, M. F. and others, "Mechanical Fracture Predictions for Sensitized Stainless Steel Piping with Circumferential Cracks", Final Report, EPRI NP-192, September 1976.
- 4.2 Kanninen, M. F. and others, "Instability Predictions for Circumferentially Cracked Type 304 Stainless Steel Pipes Under Dynamic Loadings", Final Report on EPRI Project T118-2, by Battelle Columbus Laboratories, EPRI Report Number NP-2347, April 1982.
- 4.3 Wilkowski, G. M. and others, "Degraded Piping Program -Phase II", Semiannual Report, April 1986-September 1986, by Battelle Columbus Laboratories, NUREG/CR-4082, Vol. 5, April 1987.
- 4.4 American Society of Mechanical Engineers Boiler and Pressure Vessel Code, Edition July 1989, See Section XI Article IWB-3640.
- 4.5 American Society of Mechanical Engineers Boiler and Pressure Vessel Code, Edition July 1989, See Code Case N-463.
- 4.6 Maricchiolo, C. and Milella, P. P., "Fracture Behavior of Carbon Steel Pipes Containing Circumferential Cracks at Room Temperature and 300 C", Nuclear Engineering and Design, 111, pp. 35-46, 1989.
- 4.7 Wilkowski, G. M. and Eiber, R. J., "Evaluation of Tensile Failure of Girth Weld Repair Grooves in Pipe Subjected to Offshore Laying Stresses", ASME Journal of Energy Resources Technology, Vol. 103, March 1981.
- 4.8 Kurihara, R., Ueda, S. and Sturm, D., "Estimation of the Unstable Fracture of Pipe with a Circumferential Surface Crack Subjected to Bending", Nuclear Engineering and Design, 1987.
- 4.9 Wilkowski, G. M. and others, "Degraded Piping Program - Phase II", Summary of Technical Results and Their Significance to Leak-Before-Break and In-Service Flaw Acceptance Criteria, March 1984-January 1989, by Battelle Columbus Division, NUREG/CR-4082, Vol. 8, March 1989.
- 4.10 Kumar, V., German, M. D., and Shih, C. F., "An Engineering Approach for Elastic-Plastic Fracture Analysis", EPRI report NP-1931, July 1981.
- 4.11 Zahoor, A. and Gamble, R. M., "Evaluation of Flawed-Pipe Experiments", EPRI report NP-4883M, November 1986, see Section 2.0.
- 4.12 Paris, P. C. and Tada, H., "The Application of Fracture Proof Design Methods Using Tearing Instability Theory to Nuclear Piping Postulating Circumferential Through-Wall Cracks", NUREG/CR-3464, September 1983.
- 4.13 Klecker, R. and others, "NRC Leak-Before-Break (LBB.NRC) Analysis Method for Circumferentially Through-Wall-Cracked Pipes Under Axial Plus Bending Loads", NUREG/CR-4572, May 1986.

- 4.14 Brust, F. W., "Approximate Methods for Fracture Analyses of Through-Wall Cracked Pipes". NRC Topical Report by Battelle Columbus Division, NUREG/CR-4853, February 1987.
- 4.15 Gilles, P. and Brust, F. W., "Approximate Fracture Methods for Pipes, Part 1, Theory," Nuclear Engineering and Design, 127, pp. 1-17, 1991.
- 4.16 Milne, I., Ainsworth, R. A., Dowling, A. R., and Stewart, A. T., "Assessment of the Integrity of Structures Containing Defects", R/H/R6-Rev. 3, Published by Central Electric Generating Board, England, May 1986.
- 4.17 Kumar, V. and German, M., "Elastic-Plastic Fracture Analysis of Through-Wall and Surface Flaws in Cylinders", EPRI report NP-5596, January 1988.
- 4.18 Scott, P. M., and Ahmad, J., "Experimental and Analytical Assessment of Circumferentially Surface-Cracked Pipes Under Bending," NUREG/CR-4872, April 1987.
- 4.19 Kanninen, M. F. and others, "The Development of a Plan for the Assessment of Degraded Nuclear Piping by Experimentation and Tearing Instability Fracture Mechanics Analysis", NUREG/CR-3142, June 1983.
- 4.20 Pan, J., "Some Considerations on Estimation of Energy Release Rates for Circumferentially Cracked Pipe", in Journal of Pressure Vessel Technology, Vol. 106, November 1984, pp. 391-398.
- 4.21 Olson, R., Scott, P., Marschall, C., and Wilkowski, G., "Comparison of Fracture Toughness Values from Large-Scale Pipe System Tests and C(T) Specimens", in Proceedings of the Joint IAEA/CSNI Specialists' Meeting on Fracture Mechanics Verification by Large Scale Testing, NUREG/CP-0131, pp 841-854, October 1992.

5.0 DISCUSSION OF TECHNICAL RESULTS

The technical results from the material characterizations, pipe experiments, and analyses are summarized below.

5.1 Technical Results from Material Characterization Efforts

The materials used in this effort were the same as those used in IPIRG Subtask 1.2, Stability of Cracked Pipe Under Seismic/Dynamic Displacement-Controlled Stresses, Refs. 5.1 and 5.2. Material characterization results from that document are repeated in this report for completeness.

The two materials investigated in this effort, an A106 Grade B carbon steel and a Type 304 austenitic stainless steel, exhibited markedly different responses to increased displacement rates in mechanical property tests. The carbon steel, because of its susceptibility to dynamic strain aging, showed marked effects of strain rate in tensile tests. Of special importance to nuclear piping applications, both the tensile strength and fracture elongation at 288 C (550 F) were lowered significantly by a four-to-five order of magnitude increase in the strain rate. The stainless steel, on the other hand, showed virtually no change in tensile strength or fracture elongation as strain rate was increased, but did show a significant increase in yield strength.

With respect to fracture toughness, the effects of increasing displacement rate by a factor of approximately 3,000 were relatively modest in both materials. The J-R curve was raised slightly by testing at dynamic rates in the stainless steel, but was lowered slightly in the carbon steel. Crack instabilities at 288 C (550 F), which have frequently been observed in carbon steel tests at quasi-static rates, and are assumed to be associated with susceptibility to dynamic strain aging, were observed in the carbon steel studied here at both quasi-static and dynamic displacement rates.

The relatively modest effect of strain rate on the fracture toughness of the carbon steel (Pipe DP2-F30) differed from that observed for larger-diameter carbon steel pipes investigated in Subtask 1.3 of the IPIRG program. In that study, dynamic loading rates significantly lowered the J-R curve for Pipe DP2-F29 and significantly raised the J-R curve for a submerged-arc weld in that pipe (DP2-F29W). The reasons for the different responses to increased strain rate in different carbon steels is not presently understood, but is believed to be associated with the details of their susceptibility to DSA.

These results suggest that some of the carbon steel materials may have lower toughness at dynamic rates. Additional data needs to be developed to assess this general effect, and to assess if such dynamic testing needs to be conducted for LBB or for modifying in-service flaw evaluation criteria.

5.2 Technical Results from Pipe Fracture Experiments

One of the most important observations that can be made from these experiments, is that for the inertial experiments, once maximum load was achieved, there were very few additional cycles until the pipe essentially reached a DEGB. For five of the six inertial-cracked-pipe experiments, there were only 2 or 3 cycles past maximum load until the pipe specimens reached a DEGB condition. The stainless steel TWC experiment was the exception. This experiment was stopped shortly past maximum load before significant crack growth could occur. Hence, the number of cycles it could sustain past maximum load is not known. Seismic events may have roughly 5 to 30 large amplitude cycles. Hence, with several cycles available to produce damage, a seismic loading of a piping system loaded mainly by pressure and inertial loads would be likely to produce failure if maximum load is reached at any time during the event. Consequently, the stability of pipe under pure inertial loading, or inertial stresses combined with other load-controlled stresses (i.e., dead-weight and pressure stresses), should be considered as being closer to load-control than displacement-control for typical quasi-static pipe fracture analyses.

In these inertially loaded pipe experiments, there were two factors which were different from typical quasi-static pipe fracture experiments. The first was that the loading rate was about 1,000 times faster than in a typical quasi-static four-point bend pipe fracture experiment. This change in strain rate appears to have little effect on wrought stainless steel base metal properties, but can effect the strength and toughness of ferritic pipes susceptible to dynamic strain aging at LWR temperatures.

The second factor is the influence of the cyclic loading on the fracture resistance of the material. From the IPIRG Subtask 1.2 efforts, Refs. 5.1 and 5.2, there are two aspects of the cyclic load history that are important. The first is the stress ratio (R), i.e., the ratio of the minimum to maximum stresses. The second is the incremental plastic displacement. Both of these parameters were variable in the inertially loaded pipe fracture experiments. Figure 5.1 is a plot of the effective stress ratio as a function of cycle number for the carbon steel through-wall-crack inertial experiment. The effective stress ratio is based on total stress values for a given cycle. The minimum and maximum stress values include both the bending stress components as well as the axial tension stress due to internal pressure. From Figure 5.1 it can be seen that the effective stress ratio was near one at the start of the test and slowly decreased towards zero near the end of the test. The literature review conducted in Reference 5.3 indicated that for stress ratios greater than zero there should be essentially no effect on the J-R curve, other than from fatigue crack growth considerations. However, for stress ratios where complete crack closure occurs (approaching $R = -1$) there is an additional detrimental effect of the cyclic loading on the fracture resistance, i.e., there is some significant compressive damage that occurs in the crack tip region. This is illustrated in Figure 5.2 which shows the J-R curves for three different stress ratios for the stainless steel pipe experiments from References 5.1 and 5.2.

As noted above, the second parameter associated with the cyclic load history to be considered is the incremental plastic displacement relative to the displacement to reach crack initiation under monotonic loading. In Reference 5.1, it was found that the fracture resistance, in terms

Figure 5.1. Effective stress ratio as a function of cycle number for Experiment 1.1-2

I-5/91-G10-PS

Figure 5.2. J-R curves for the TP304 stainless steel pipe fracture experiments using the η -factor approach, Ref. 5.2

I1.2--8/89-F4.2

Figure 5.3. Crack-tip-opening-angle data from IPIRG Subtask 1.2 stainless steel pipe experiments, Ref. 5.1

11.2-6/91-F5.3

of the crack-tip-opening angle (CTOA), varied strongly as a function of the incremental plastic displacement. This is illustrated in Figure 5.3 which comes from Reference 5.1. In the inertial experiments, the incremental plastic displacement increased with time. At the beginning of the test the incremental plastic displacement is small, i.e., the potential exists for cyclic damage based on Figure 5.3. However, at the beginning of the inertial test, the stress ratio is near one. Consequently there should not be an effect of cyclic load history. At the end of the test, the stress ratio decreases to near zero, but by this time the incremental plastic displacement is large. Near the end of the test, the ratio of the displacement at crack initiation to the incremental plastic displacement is in the range of 2 to 5 for the Subtask 1.1 inertial experiments. Looking at Figure 5.3, values of 2 to 5 on the x-axis show little effect on the CTOA from cyclic loading compared to the monotonic CTOA. Hence, the load histories used in the Subtask 1.1 inertial experiments should not have caused any significant degrading effect on the fracture resistance. One point of caution with regard to this discussion – Figure 5.3 is an empirical relationship developed as part of Subtask 1.2 for stainless steel through-wall cracks. Further work is needed to assess if this relationship can be generalized for other materials and surface crack geometries.

In summary, it appears that the cyclic load histories used in these experiments were not as severe as the $R=-1$ loading used in the IPIRG Subtask 1.2 experiments from a viewpoint of inducing damage and reducing the fracture toughness of the materials. Consequently, these experiments could effectively be analyzed using monotonic specimen J-R curve data.

However, dynamic strain aging of the ferritic steel suggests that J-R curve data and tensile test data may need to be acquired at the appropriate strain rate.

Another point of interest in reviewing the results from the Subtask 1.1 pipe experiments is that the dynamic monotonic, carbon steel, surface-crack experiment (1.1-9) exhibited a higher load-carrying capacity than the quasi-static monotonic, carbon steel, surface-crack experiment (1.1-7). The ratio of the experimental stress at surface-crack penetration to the predicted stress from the various analysis methods considered was higher for the dynamic experiment than it was for the quasi-static experiment. This is contrary to what one would expect on the basis of the material property data. Both the strength and C(T) specimen toughness of this carbon steel material decreased at the higher loading rates. This apparent contradiction has been attributed to scatter in the experimental results. The effect of scatter is an especially important consideration when considering the results from pipe tests for carbon steels which are known to be susceptible to dynamic strain aging effects. As part of the "Short Cracks in Piping and Piping Welds" Program, Ref. 5.4, it was shown that elevated temperature hardness data for this 6-inch nominal diameter pipe varied considerably. Consequently, one may expect significant differences in strength properties at different locations along the same length of pipe. As a result, scatter in the experimental results for experiments using the same heat of this pipe should be expected.

5.3 Technical Results from Analytical Evaluations

The following points can be summarized from the analysis aspects from Section 4 of this report.

5.3.1 Net-Section-Collapse Analysis of Experiments

The A106 Grade B carbon steel pipe experiments tended to fail below the Net-Section-Collapse (NSC) analysis predicted stresses when using actual measured tensile properties to define the flow stress. This is to be expected since the toughness of this carbon steel material is sufficiently low that fully plastic conditions are not being satisfied. Conversely, when the NSC predictions are based on the ASME Section XI 2.4S_m definition of flow stress, then the experimental stresses are above the NSC predictions. This is an artifact of the fact that the actual strength of this carbon steel (DP2-F30) is significantly higher than expected on the basis of Section III Code values for S_m.

It is also of note that the ratios of the experimental-to-predicted NSC stresses were lower for the through-wall-cracked-pipe experiments than they were for the surface-cracked-pipe experiments. This was expected since past test results showed that through-wall-cracked pipes are more sensitive to toughness than surface-cracked pipe.

5.3.2 Dimensionless Plastic-Zone Parameter Criterion

The Dimensionless Plastic-Zone Parameter (DPZP) Criterion is a semi-empirical relationship in which the NSC predicted stresses are adjusted to account for the conditions where fully plastic conditions do not exist. For the relatively small diameter, high toughness, Subtask 1.1 stainless steel pipe experiments, fully plastic conditions exist. Consequently no adjustment is needed for the NSC predictions. As a result, the NSC and DPZP predictions are identical for these experiments. For the low toughness, Subtask 1.1 carbon steel experiments, contained plasticity conditions exist. The ratio of the calculated plastic-zone size to the remaining tensile ligament was approximately 0.085. As such an adjustment is needed for the NSC predictions. When such an adjustment was made for the Subtask 1.1 carbon steel experiments, the predicted stresses were typically less than the experimental stresses.

5.3.3 J-Estimation Scheme Predictions of Crack Initiation and Maximum Stresses

Various elastic-plastic fracture estimation schemes were used with laboratory specimen data to predict the stresses at crack initiation and maximum stress for the Subtask 1.1 cracked-pipe experiments. For the through-wall-cracked-pipe experiments, predictions using the GE/EPRI and LBB.ENG2 methods were compared with the experimental results. For the surface-cracked-pipe experiments, the SC.TNP analysis predictions were compared with the experimental results. These calculations were conducted using both quasi-static and dynamic properties, as well as using J_D -R and J_M -R curves, although for the surface-cracked pipe analyses the choice of the J-R curve is immaterial. These results showed the following.

5.3.3.1 Through-Wall-Cracked-Pipe Analyses

For all cases (both the GE/EPRI and LBB.ENG2 methods for both quasi-static and dynamic properties and both J_D - and J_M -R curves) the predicted stresses were less than the experimental stresses. The GE/EPRI method predicted lower stresses than the LBB.ENG2 method. This agrees with trends observed in analyzing pipe experiments from the Degraded Piping Program, Ref. 5.5, and IPIRG Subtask 1.2, Ref. 5.2. Using the J_M -R curve gave higher maximum stress predictions than using the J_D -R curve. The use of dynamic material property data had a small effect (5 to 15 percent) on the predictions. For the carbon steel experiments the use of dynamic material properties resulted in slightly lower predictions for both the GE/EPRI and LBB.ENG2 methods. This is to be expected since the yield and ultimate strengths as well as the J-R curve were lower for this material at dynamic loading rates. For the stainless steel experiments, the use of dynamic material properties had almost no effect on the GE/EPRI predictions of maximum stress and a very small effect (5 to 10 percent) on the LBB.ENG2 predictions of maximum stress. For the stainless steel, the ultimate strength decreased slightly at dynamic loading rates while the J-R curve increased slightly. The close agreement between the quasi-static and dynamic predictions for both materials indicates that the use of standard quasi-static material property data for dynamic analyses of these experiments is probably sufficient.

5.3.3.2 Surface-Cracked-Pipe Analyses

The SC.TNP analysis was used to predict the initiation and maximum stresses for a number of the Subtask 1.1 surface-cracked pipe experiments. In Reference 5.6, this analysis was found to generally predict maximum stresses within 20 percent of the experimental values for pipes subjected to pure four-point bending. In order to analyze the Subtask 1.1 experiments, the SC.TNP analysis method had to be corrected to account for the fact that the method, in its current form, does not include the effect of internal pipe pressure or tension on J . The effect of internal pressure was addressed in an approximate way by replacing the axial tension in the uncracked ligament ahead of the crack by an additional applied bending moment.

For the IPIRG Subtask 1.1 surface-cracked-pipe experiments, the SC.TNP predictions of maximum stress tended to be lower (15 to 20 percent) than the experimentally determined values. This agrees with the findings from Reference 5.6 for the case of pure bending. As a result, it may be concluded that the equivalent moment correction made for accounting for the effect of internal pressure is reasonable for engineering applications.

5.3.4 R6 Revisions Option 1 Predictions

For the R6 Option 1 method, stresses were calculated at both crack initiation and maximum stress using quasi-static material property data. The predictions were compared to stresses calculated from linear elastic stress analyses instead of the experimentally determined stresses. It was found that the R6 Option 1 method consistently underpredicted the elastically calculated stresses both at crack initiation and maximum stress. For the four inertially loaded experiments, the R6 Option 1 method significantly underpredicted the elastically calculated maximum stress. The ratios of elastically calculated maximum stress to the predicted maximum stress for the four inertial experiments ranged from 1.69 to 3.93. The highest ratios were for the two stainless steel experiments. This is not surprising in that the two stainless steel experiments exhibited much more nonlinear behavior before maximum load was reached than did the carbon steel experiments. Consequently, one would expect that a linear elastic analysis of these stainless steel experiments would overpredict the experimental values. The ratios of the elastically calculated stresses at crack initiation to the R6 predicted stress at crack initiation were much lower than the corresponding ratios at maximum stress. The extent of nonlinear behavior is much less at crack initiation than it is at maximum stress. The degree of underprediction was much less for the two displacement-controlled experiments.

5.3.5 ASME Section XI Predictions

Comparisons of the maximum experimental stresses were made to the ASME Code Section XI in-service flaw evaluation procedures. These comparisons did not include the ASME Code safety factors of 2.78 for normal and test conditions, or the 1.39 safety factor for emergency and faulted conditions. Predictions of maximum stress for the Section XI procedures were made for two different definitions of flow stress. In one case the flow stress was based on actual Code values of S_m [$S_m(\text{Code})$]. The flow stress was defined as either $3S_m$ for the austenitic experiments, or $2.4S_m$ for the ferritic experiments, where the S_m values were taken

directly from Section III of the Code. For the other case, actual material property data were used to define the S_m values [$S_m(\text{Actual})$], and thus the flow stress. This was an attempt to account for the fact that the pipes evaluated in this program (particularly the A106B carbon steel pipe) had higher strength properties than what might be expected on the basis of Section III S_m values.

As a result of these comparisons it was shown that the predictions from the austenitic criterion (IWB-3640) were in close agreement (within 10 percent) with the experimental results. Also, the predicted stresses were typically less than the experimental values. One wonders if this finding holds true for larger diameter, possibly less tough, stainless steel pipe for which contained plasticity conditions might exist. Also, what effect would a different, possibly more damaging, cyclic load history have on this finding? Such questions cannot be addressed with the limited data base existing today.

It was found that the predictions from the ferritic criterion (IWB-3650) were significantly lower than the experimentally determined maximum stresses. When Code values of S_m were used to define the flow stress, the experimental stresses were 87 to 151 percent higher than the predicted values. When actual properties were used to define the flow stress, the experimental stresses were 32 to 77 percent higher than the predicted values. This finding is consistent with the trend of the results of the IPIRG Subtask 1.2 pipe fracture experiments where the experimental stresses were 35 to 78 percent higher than the Code predicted values when Code values of S_m were used to define the flow stress. The fact that the degree of underprediction is somewhat greater for the Subtask 1.1 experiments than for the Subtask 1.2 experiments is to be expected in that all but one of the Subtask 1.1 carbon steel experiments were surface-cracked-pipe experiments while all of the Subtask 1.2 experiments were through-wall-cracked-pipe experiments. It has been shown that the through-wall cracks tend to be more sensitive to toughness effects than do the surface cracks, see Figure 4.4.

5.3.6 Calculation of J_i Values from Pipe Experiments

Values of J at crack initiation were calculated using the η -factor method for one of the Subtask 1.1 through-wall-cracked-pipe experiments and three of the surface-cracked-pipe experiments. There was variable agreement between the pipe experiment and C(T) specimen test J_i values which requires a test by test assessment.

It should be noted that the pipe experiments conducted in Subtask 1.1 were on a relatively small diameter pipe (6-inch nominal diameter). Hence, the effects of toughness variations on loads in these experiments is expected to be small. The reason for evaluation of the toughness from these pipe experiments is to assess if there may be a consistent trend of toughness from the pipe experiments with different flaw geometries, loading histories, and material relative to the standard C(T) test data. The C(T) specimens are typically used for evaluation of larger diameter pipe that would have loads more sensitive to toughness changes. The findings from Section 4 of this report are summarized as follows.

The TP304 stainless steel TWC inertial test has a slightly lower than expected J-R curve that is not explainable from the higher loading rates or the magnitude of the cyclic loading, i.e.,

the load-ratio (R) was greater than zero. The decrease in the J_i value and J-R curve was about 40 percent compared to the quasi-static pipe test result, but this was close to the side-grooved C(T) specimen J-R curve.

For the surface-cracked inertially loaded TP304 pipe experiment, the calculated J_i from the pipe experiment was about a factor of 4 lower than the C(T) specimen J_i value. In this test, the load-ratio was -0.27. This is relatively close to the surface-cracked pipe system experiment in IPIRG Subtask 1.3, Ref. 5.7. The implication from this result is that either: (1) there is a sharp transition of the change in toughness from $R = 0$ to $R = -0.27$, or (2) surface-cracked pipe are more susceptible to cyclic toughness degradation than TWC pipe. From past quasi-static pipe experiments, it is anticipated that the apparent toughness for a surface-cracked pipe may, on average, be four times higher. This increase in the apparent toughness is believed to be due to loss of constraint through the thickness, and is a subject of current research. Hence, the cyclic effects appear to also have negated the beneficial effect of the enhanced toughness from the constraint loss.

Experiment 1.1-7 was a surface-cracked-pipe test under pressure and dynamic monotonic loading on A106 Grade B pipe that was sensitive to slight toughness degradation at higher loading rates in C(T) specimen tests. There was no cyclic loading. The pipe J_i value was close to what was expected. The toughness was elevated relative to the C(T) specimen due to constraint loss for the surface-crack geometry, but the increase was slightly less than expected due to detrimental loading-rate effects on the A106 Grade B pipe material. The net result was a toughness increase by a factor of 2.87.

Experiment 1.1-9 was a quasi-static companion to Experiment 1.1-7. This experiment has a significantly lower J_i value than expected. The failure load was also lower than Experiment 1.1-7, even when accounting for differences in the crack depth. Since the quasi-static toughness should be higher, not lower, this result is unexplainable other than by the higher than normal scatter in material properties observed in other tests on this same length of pipe.

In addition, the full J-R curve was calculated from the moment-rotation crack growth data from Experiment 1.1-3. The J-R curve for this stainless steel through-wall-cracked inertial pipe experiment was quite a bit lower than the J-R curve from the companion quasi-static, displacement-controlled pipe experiment from the Degraded Piping Program (4131-5) and slightly lower than the J-R curve from the quasi-static C(T) specimen test. On first glance, it may appear that the lower J-R curve for the Subtask 1.1 inertial experiment was a result of the cyclic load history for this experiment. However, as previously noted, the cyclic load history for this inertial experiment was not that severe. The value of the incremental plastic displacement was relatively large and the stress ratio was positive throughout the test. Thus, it appears that the cyclic load history is not the explanation. The second possible explanation is that the J-R curve is lowered as a result of loading rate effects. However, for this stainless steel material, it has been shown that the effect of loading rate on the strength and toughness is minimal. As a result, the reason for this discrepancy is unresolved at this time. However, the toughness decrease in this case was not highly significant compared to the other increases (from geometry) or cyclic loading on the surface-cracked-pipe tests.

5.3.7 Dynamic Analyses of Pipe Fracture Experiments

The nonlinear spring element representation of a cracked pipe in the ANSYS code was originally developed to help design the IPIRG Subtask 1.1 inertial experiments and the Subtask 1.3 pipe system experiments. This analysis procedure, however, was also found to provide a very close prediction of the inertial pipe test dynamic results. The accuracy of this method, however, depends on the accuracy of the J-estimation scheme used to predict the moment-rotation relationship of the cracked pipe section. If the dynamic load history had caused cyclic loads with highly negative stress ratios or small increments of plastic displacement, then the monotonic J-R curve used in the J-estimation scheme could cause an overprediction of the dynamic load-carrying capacity of the pipe.

Another interesting aspect of these calculations from a dynamic analysis viewpoint is obtained when the experimentally measured moments are compared with linear elastically calculated moments. The linear elastic moments were calculated using uncracked pipe with the actual experimental displacement function. Ratios of the moments from elastic stress analyses to the experimental moments for the four inertial experiments ranged from 1.2 to 1.9. For the two displacement-controlled experiments, these ratios were less. These ratios for the Subtask 1.1 experiments are much lower than what was found from the EPRI/NRC Piping and Fitting Dynamic Reliability Program (Ref. 5.8). It is anticipated that if the crack size evaluated in Subtask 1.1 had been smaller, then the ratios would increase due to plasticity of the adjacent uncracked pipe. This is shown schematically in Figure 5.4. Furthermore, the pipe system geometry could also come into play. For instance, if the high stresses were concentrated at only one location, then the discrepancy in the elastic analysis would be less than if there were multiple points of high plastic stress or strain in the piping system.

Figure 5.4. Illustration of postulated trends in discrepancy for elastic stress analysis

I1.2-6/91-F5.4

5.4 References

- 5.1 Scott, P., Kramer, G., Veith, P., Francini, R., and Wilkowski, G., "The Effect of Dynamic and Cyclic Loading During Ductile Tearing on Circumferentially Cracked Pipe – Experimental Results", ASME PVP Vol. 280, June 1994, pp 207-220.
- 5.2 Wilkowski, G., Kramer, G., Vieth, P., Francini, R., and Scott, P., "The Effect of Cyclic Loading During Ductile Tearing on Circumferentially Cracked Pipe – Analytical Results", ASME PVP, Vol. 280, June 1994, pp 221-240.
- 5.3 Marschall, C. and Wilkowski, G., "Effect of Cyclic Loads on Ductile Fracture Resistance", in ASME PVP, Vol. 166, pp 1-14, July 1989.
- 5.4 Wilkowski, G. M., and others, "Short Cracks in Piping and Piping Welds", by Battelle, NUREG/CR-4599, Vol. 2, No. 1.
- 5.5 Wilkowski, G. M., and others, "Degraded Piping Program -Phase II", Summary of Technical Results and Their Significance to Leak-Before-Break and In-Service Flaw Acceptance Criteria, March 1984-January 1989, by Battelle Columbus Division, NUREG/CR-4082, Vol. 8, March 1989.
- 5.6 Scott, P. M. and Ahmad, J., "Experimental and Analytical Assessment of Circumferentially Surface-Cracked Pipes Under Bending", NUREG/CR-4872, April 1987.
- 5.7 Olson, R., Scott, P., Marschall, C., and Wilkowski, G., "Comparison of Fracture Toughness Values from Large-Scale Pipe System Tests and C(T) Specimens", in proceedings of IAEA/CSNI Specialists Meeting on Fracture Mechanics Verification by Large Scale Testing, editor Dr. Claud Pugh, ORNL, Oak Ridge, Tenn., October 1992.
- 5.8 DeVita, V., "Seismic Fragility Pipe Testing", Transaction of the 10th International Conference on Structural Mechanics in Reactor Technology, Vol. S, p 81, August 1989.

6.0 SIGNIFICANCE OF RESULTS ON LBB AND IN-SERVICE FLAW EVALUATION CRITERIA

6.1 Use of Linear-Elastic Stress Analysis for LBB and In-Service Flaw Evaluations

The experimental and analytical results for the Subtask 1.1 experiments showed that the maximum experimental moments and stresses were less than the values calculated using linear-elastic stress analysis. However, the extent of the discrepancies between the elastic stress analyses and the corresponding experimental results were not nearly as large as those reported for uncracked pipe. For the Subtask 1.1 experiments, with relatively large cracks, the ratio of the calculated linear-elastic stress to the experimental stress was less than 2. Results from the EPRI/NRC Piping and Fitting Dynamic Reliability Program suggest that this ratio would be significantly higher for uncracked piping system experiments (Ref. 6.1). It seems certain that the ratio of elastic to measured stress would be larger for cracked pipe experiments with smaller flaws than those evaluated in Subtask 1.1, i.e., LBB size through-wall cracks or short surface cracks. An assessment of this discrepancy for linear elastic stress analyses of LBB size cracks would be desirable. Such an assessment would be easier in an inertial experiment, such as those conducted in Subtask 1.1, than in a pipe system experiment, such as those conducted in Subtask 1.3 (Ref. 6.2). Furthermore, development of a methodology to define the degree of this discrepancy for elastic stress analysis in a simple method applicable to engineering approaches may be a worthwhile endeavor. This would help evaluate proposed changes in rules for pipe stress analysis design as a result of the EPRI/NRC Piping and Fitting Dynamic Reliability Program.

6.2 Cyclic Load History Considerations for Both LBB and In-Service Flaw Evaluations

A separate aspect of potential importance is the effect of cyclic load histories on the degradation of the material toughness during a seismic event. Typically, LBB, or in-service flaw evaluations, will consider the subcritical crack growth up to the time that a seismic event will occur. The fracture analysis will consider the peak seismic load as a statically applied load. However, the effect of the cyclic loads during the seismic event, either in terms of additional subcritical crack growth, or material degradation is not considered.

It was found that if the stress ratio is negative, and the incremental plastic displacement is small (relative to the monotonic displacement required to reach the start of ductile tearing), then the apparent toughness of the material can be degraded by the cyclic load history (Refs. 6.3 and 6.4). However, based on these findings from Subtask 1.2, it was concluded that the inertially loaded TWC experiments conducted in this effort had load histories that were not severe enough to cause the toughness degradation observed in some of the IPIRG Subtask 1.2 experiments. However, the stainless steel surface crack inertial pipe test did show a significantly reduced toughness in the pipe test compared to the C(T) test result. This is consistent with the IPIRG Subtask 1.3 stainless steel pipe system test suggesting surface-cracked pipe may be more sensitive to toughness degradation from cyclic loading than TWC

pipe. Nevertheless, it may be desirable to determine if typical seismic load histories for typical plant piping systems could be severe enough to cause degradation of the material toughness. One word of caution is in order in this regard. The empirical relationship developed from the IPIRG Subtask 1.2 experimental data is based on stainless steel through-wall-crack pipe data. Further work is needed to assess if this relationship is valid for other materials and surface crack geometries. It may be found that for surface cracks, or for carbon steel materials, that the cyclic load history effect on toughness is quite different.

The load histories used in the Subtask 1.1 inertial experiments were designed to minimize the concern of low-cycle fatigue-crack growth during the experiments. Other similar experimental efforts, Refs. 6.5 and 6.6, used either constant amplitude "sine bursts" or "seismic bursts" for their inertial load histories. In those experiments, significant low-cycle fatigue-crack growth reduced the experimental failure stresses well below that predicted by the Net-Section-Collapse (NSC) analysis when the initial flaw size was used in the NSC analysis, see Figure 6.1. The effect of this low-cycle fatigue-crack growth during the seismic event is currently not included in LBB or the ASME flaw evaluation criteria. This effect is especially important in the ASME flaw evaluation criteria for surface cracks since a small amount of low-cycle fatigue-crack growth will represent a significant portion of the remaining ligament for the cracked pipe section.

6.3 Significance to In-Service Flaw Evaluation Criteria

In-service flaw evaluation procedures deal with surface-cracked pipe. In the ASME Code, there are typically different applied safety factors for Service Levels A and B which includes normal, test, and OBE conditions and Service Levels C and D which includes emergency (SSE) and faulted conditions. The ASME Section XI flaw evaluation procedure, for instance, uses a safety factor of 2.78 for Service Levels A and B conditions and 1.39 for Service Levels C and D conditions. The difference in safety factor is partially due to the fact that Service Levels A and B conditions include all design transients expected to occur during plant operation so that the system is designed to accommodate them while Service Levels C and D conditions refers to a very low probability event whose consequences are such that subsequent plant operation is not required and safe system shutdown is the only consideration.

6.3.1 ASME Section XI Stainless Steel Pipe Evaluations

The results of this effort showed that for a stainless steel pipe that has properties close to the ASME Section II values, using the ASME Code definition of flow stress ($3S_m$) in a Section XI in-service flaw evaluation resulted in predictions that were slightly less than the experimental values. This assessment did not include the ASME Code safety factors of 1.39 or 2.78. Consequently, the actual safety factors for these Subtask 1.1 stainless steel experiments were somewhat greater than the applied safety factors.

(a) Comparison between experiment and limit-load analysis by the formulation of Hasegawa, et al.

(b) Comparison between experiment and limit-load analysis by the formulation of Kurihara and Ueda

Figure 6.1. Comparison of STA pipe system test failure loads with modified Net-Section-Collapse analyses using flaw size prior to dynamic load (From Ref. 6.5)

II.3-10/90-F1.7

6.3.2 ASME Section XI Carbon Steel Pipe Evaluations

Contrary to the IPIRG Subtask 1.2 results, the results of this effort showed that for an A106 Grade B pipe that has properties close to the ASME Section III values (i.e., when S_m (actual) was used in the analysis), using the ASME Code approach would still result in significant underpredictions of the experimental maximum stresses. The fact that the IWB-3650 procedures consistently underpredicted the experimental results for the Subtask 1.1 experiments while tending to overpredict the results for the Subtask 1.2 experiments is possibly to be expected. All but one of the Subtask 1.1 carbon steel experiments were surface-cracked-pipe experiments while all of the Subtask 1.2 experiments were through-wall-cracked-pipe experiments. It has been shown that the through-wall cracks tend to be more sensitive to toughness effects than do the surface cracks. However, if a more severe cyclic load history were applied to similar experiments on inertially loaded surface-cracked pipe, the Subtask 1.1 surface-cracked-pipe experiments may also fail at or below the IWB-3650 predicted levels.

6.3.3 R6 Revision 3 Option 1 Assessment

As part of this effort, it was found that the R6 Revision 3 Option 1 method consistently underpredicted the elastically calculated stresses. However, for the two displacement-controlled experiments, the degree of underprediction was only 25 to 45 percent (compared with 90 to 300 percent underpredictions for the inertial experiments). The fact that the R6 predictions are so close to the elastic results for these two displacement-controlled experiments does raise an interesting question. Paragraph 6.1.1 of the R6 document states that the failure assessment diagram for Option 1 "provides a reasonable underestimate of the flaw tolerance of a structure". The question which naturally arises is, is a 25 to 45 percent underestimate reasonable? This is the type of safety factor Section XI of the ASME Code incorporates in its in-service flaw assessment criteria for emergency and faulted conditions, but is only about half of the safety factor incorporated for normal (including upset and test) conditions.

6.4 References

- 6.1 DeVita, V., "Seismic Fragility Pipe Testing", Transaction of the 10th International Conference on Structural Mechanics in Reactor Technology, Vol. S, p 81, August 1989.
- 6.2 Schmidt, R. A., Wilkowski, G. M., and Mayfield, M. "The International Piping Integrity Research Group (IPIRG) Program--An Overview", SMiRT-11 Proceedings, Paper G23/1, August 1991.
- 6.3 Scott, P., Kramer, G., Veith, P., Francini, R., and Wilkowski, G., "The Effect of Dynamic and Cyclic Loading During Ductile Tearing on Circumferentially Cracked Pipe -- Experimental Results", ASME PVP Vol. 280, June 1994, pp 207-220.

- 6.4 Wilkowski, G., Kramer, G., Vieth, P., Francini, R., and Scott, P., "The Effect of Cyclic Loading During Ductile Tearing on Circumferentially Cracked Pipe -- Analytical Results", ASME PVP Vol. 280, June 1994, pp 221-240.
- 6.5 Hale, D. A., and others, "The Growth and Stability of Stress Corrosion Cracks in Large-Diameter BWR Piping", EPRI Report NP-2472, Vol. 2, see Appendices B and C, July 1982.
- 6.6 Murakami, E., and others, "Crack Growth of Nuclear Piping Under Dynamic Loading", ASME PVP special publication PVP 167, pp. 115-120, July 1989.

APPENDIX A
SPECIMEN GEOMETRY DIMENSIONS
FOR J-R CURVE TESTS

APPENDIX A

SPECIMEN GEOMETRY DIMENSIONS FOR J-R CURVE TESTS

Table A.1. Compact specimens machined from Pipe DP2-F30, A106 Grade B carbon steel (L-C orientation)

Specimen Ident. No.	Test Speed ^(a)	% Side Grooves	Specimen Dimensions, mm (inches)			
			W	B	B _N	a ₀
F30-ZP15-5LC ^(b)	QS	20	25.4 (1.00)	9.12 (0.359)	7.30 (0.287)	13.46 (0.530)
F30-ZP15-6LC ^(b)	QS	20	25.4 (1.00)	9.12 (0.359)	7.30 (0.287)	13.28 (0.523)
F30-112	QS	23	25.4 (1.00)	12.67 (0.499)	9.78 (0.385)	14.10 (0.555)
F30-114	QS	23	25.4 (1.00)	12.70 (0.500)	9.78 (0.385)	14.35 (0.565)
F30-113	QS	23	25.4 (1.00)	12.70 (0.500)	9.75 (0.384)	14.55 (0.573)
F30-115	QS	23	25.4 (1.00)	12.70 (0.500)	9.73 (0.383)	14.48 (0.570)
F30-108	Dyn	23	25.4 (1.00)	12.67 (0.499)	9.73 (0.383)	14.20 (0.559)
F30-109	Dyn	23	25.4 (1.00)	12.65 (0.498)	9.78 (0.385)	14.33 (0.564)
F30-110	Dyn	23	25.4 (1.00)	12.67 (0.499)	9.73 (0.383)	14.12 (0.556)
F30-111	Dyn	23	25.4 (1.00)	12.67 (0.499)	9.73 (0.383)	14.43 (0.568)

(a) QS = quasi-static, Dyn = dynamic.

(b) Specimens were fabricated and tested at Materials Engineering Associates.

Table A.2. Compact specimens machined from Pipe DP2-A23, Type 304 stainless steel (L-C orientation)

Specimen Ident. No.	Test Speed ^(a)	% Side Grooves	Specimen Dimensions, mm (inches)			
			W	B	B _N	a ₀
ZP17-9LC ^(b)	QS	20	25.4 (1.00)	12.70 (0.500)	10.16 (0.400)	13.62 (0.536)
ZP17-13LC ^(b)	QS	20	25.4 (1.00)	12.70 (0.500)	10.16 (0.400)	13.84 (0.545)
A23-113	QS	23	25.4 (1.00)	12.67 (0.499)	9.65 (0.380)	15.09 (0.594)
A23-108	Dyn	23	25.4 (1.00)	12.65 (0.498)	9.80 (0.386)	15.14 (0.596)
A23-109	Dyn	23	25.4 (1.00)	12.67 (0.498)	9.68 (0.381)	14.99 (0.590)
A23-110	Dyn	23	25.4 (1.00)	12.70 (0.500)	9.73 (0.383)	14.83 (0.584)
A23-111	Dyn	23	25.4 (1.00)	12.67 (0.499)	9.75 (0.384)	14.94 (0.588)
A23-112	Dyn	23	25.4 (1.00)	12.70 (0.500)	9.73 (0.383)	14.96 (0.589)

(a) QS = quasi-static, Dyn = dynamic.

(b) Specimens were fabricated and tested at Materials Engineering Associates.

APPENDIX B

NET-SECTION-COLLAPSE ANALYSIS

APPENDIX B

NET-SECTION-COLLAPSE ANALYSIS

The Net-Section-Collapse concept for predicting the failure of a circumferentially cracked pipe was developed in EPRI Project RP585 (Ref. B.1). One major assumption in the Net-Section-Collapse analysis is that the material toughness is sufficiently high that failure is governed by the strength of the material (that is, flow stress or collapse stress) and is not sensitive to the material's toughness. Also, crack growth is not taken into account. The collapse stress is a value between the material's yield strength and ultimate strength and represents an average critical net-section stress reached throughout the flawed ligament of the structure (see Figure B.1.)

For an unpressurized pipe with a circumferential through-wall crack subjected to pure bending, an idealized bending stress was assumed in the plane of the crack (as shown in Figure B.2). A free-body analysis shows that the point of stress inversion (β in Figure B.2) is

$$\beta = \frac{\pi - \theta}{2} . \quad (B-1)$$

The bending moment predicted by the Net-Section-Collapse analysis can be determined by using Eq. B-2.

$$M = 2\sigma_f R_m^2 t (2\sin\beta - \sin\theta) \quad (B-2)$$

where

- M = Net-Section-Collapse analysis predicted bending moment
- σ_f = flow stress or collapse stress
- R_m = mean radius of the pipe
- t = wall thickness of the pipe
- θ = half the initial crack angle.

Eq. B-2 was developed by integrating the forces around the pipe circumference and assuming the pipe retains a circular cross section.

From the bending moment in Eq. B-2, the bending stress (Mc/I) predicted by the Net-Section-Collapse analysis can be calculated using Eq. B-3,

$$\sigma_{NSC} = (2\sigma_f / \pi) (2\sin\beta - \sin\theta) \quad . \quad (B-3)$$

For an unpressurized pipe with a circumferential surface crack subjected to bending, that is, pure bending, an idealized bending stress was assumed in the plane of the crack as shown in Figure B.3. A free-body analysis shows that the point of stress inversion is

$$\beta = \frac{\pi - (d/t) \theta}{2} \quad (B-4)$$

This assumes that the circumferential crack length, 2θ , is less than β ; that is, the surface crack does not extend into the compressively stressed side of the pipe. The bending moment is then determined from Eq. B-5.

$$M = 2\sigma_f R_m^2 t (2 \sin\beta - (d/t) \sin\theta) \quad (B-5)$$

where

d = depth of the surface crack.

From the bending moment in Eq. B-5 one can calculate the bending stress predicted by the Net-Section-Collapse analysis using Eq. B-6,

$$\sigma_{nsc} = \frac{2}{\pi} \sigma_f [2 \sin \beta - (d / t) \sin \theta] \quad . \quad (B-6)$$

For a pressurized pipe with a circumferential part-through surface crack subjected to bending, that is, combined pressure and bending, an idealized bending stress was assumed in the plane of the crack as shown in Figure B.4. A free-body analysis shows that the point of stress inversion in this case is

$$\beta = \frac{\pi - (d/t)\theta}{2} - \frac{\pi R_i^2 p}{4R_m t \sigma_f} \quad (B-7)$$

where

R_i = inside radius of the pipe
 p = internal pipe pressure.

Again this assumes that the circumferential crack length, 2θ , is less than β .

Knowing β , the predicted stress using the Net-Section-Collapse analysis can be calculated as above using Eq. B-6. Thus, the only difference between surface crack Net-Section-Collapse formulations for the pure bending case and the combined pressure and bending case is in the determination of the stress inversion angle β . The pressure term shifts the neutral axis down away from the crack, thus lowering the predicted moment from the Net-Section-Collapse analysis.

For the case of a pipe section with a surface crack subjected only to pressure loads, that is, pure pressure, the formulation for Net-Section-Collapse stress can be developed by rearranging Eq. B-5. Specifically, for the case of pure pressure, the applied bending moment is zero. For that to be the case Eq. B-5 indicates that

$$2 \sin \beta = (d/t) \sin \theta \quad . \quad (B-8)$$

Substituting Eq. B-7 into Eq. B-8 and solving for the axial stress, the predicted pure tension failure stress predicted by the Net-Section-Collapse analysis is

$$\sigma_{nsc} = \sigma_f \left[1 - \frac{\theta}{\pi} (d/t) - \frac{2}{\pi} \arcsin \left(\frac{d}{2t} \sin \theta \right) \right] \quad . \quad (B-9)$$

References to Appendix B

- B.1. Kanninen, M. F., Broek, D., Marschall, C. W., Rybicki, E. F., Sampath, S. G., Simonen, F. B., and Wilkowski, G. M., "Mechanical Fracture Predictions for Sensitized Stainless Steel Piping with Circumferential Cracks", Final Report, EPRI NP-192, September 1976.

Figure B.1. Net-Section-Collapse criteria
T-4574-F2.1

Figure B.2. Nomenclature and loading system for the application of the Net-Section-Collapse criterion to a pipe with a circumferential through-wall crack

T-4574-F2.2

Figure B.3. Nomenclature and loading for the application of the Net-Section-Collapse criterion to a pipe with a circumferential surface crack subjected to pure bending

SA-6/86-FA.3

Figure B.4. Nomenclature and loading for the application of the Net-Section-Collapse criterion to a pipe with a circumferential surface crack subjected to combined pressure and bending
 SA-6/86-FA.4

(INTENTIONALLY BLANK)

NRC FORM 335 (2-89) NRCM 1102, 3201, 3202		U.S. NUCLEAR REGULATORY COMMISSION		1. REPORT NUMBER (Assigned by NRC. Add Vol., Supp., Rev., and Addendum Numbers, if any.)	
BIBLIOGRAPHIC DATA SHEET <i>(See instructions on the reverse)</i>				NUREG/CR-6233 BMI-2177 Vol. 1	
2. TITLE AND SUBTITLE Stability of Cracked Pipe Under Inertial Stresses Subtask 1.1 Final Report				3. DATE REPORT PUBLISHED	
				MONTH YEAR August 1994	
5. AUTHOR(S) P. Scott, M. Wilson, R. Olson, C. Marschall, R. Schmidt, and G. Wilkowski				4. FIN OR GRANT NUMBER D1658	
				6. TYPE OF REPORT Technical	
				7. PERIOD COVERED (Inclusive Dates) Oct. 1986 - Jan. 1991	
8. PERFORMING ORGANIZATION - NAME AND ADDRESS (If NRC, provide Division, Office or Region, U.S. Nuclear Regulatory Commission, and mailing address; if contractor, provide name and mailing address.) Battelle 505 King Avenue Columbus, OH 43201					
9. SPONSORING ORGANIZATION - NAME AND ADDRESS (If NRC, type "Same as above"; if contractor, provide NRC Division, Office or Region, U.S. Nuclear Regulatory Commission, and mailing address.) Division of Engineering Office of Nuclear Regulatory Research U.S. Nuclear Regulatory Commission Washington, DC 20555-0001					
10. SUPPLEMENTARY NOTES <p>This report presents the results of the pipe fracture experiments, analyses, and material characterization efforts performed within Subtask 1.1 of the IPIRG Program. The objective of Subtask 1.1 was to experimentally verify the analysis methodologies for circumferentially cracked pipe subjected primarily to inertial stresses. Eight cracked-pipe experiments were conducted on 6-inch nominal diameter TP304 and A106B pipe. The experimental procedure was developed using nonlinear time-history finite element analyses which included the nonlinear behavior due to the crack. The model did an excellent job of predicting the displacements, forces, and times to maximum moment. The comparison of the experimental loads to the predicted loads by the Net-Section-Collapse (NSC), Dimensionless Plastic-Zone Parameter, J-estimation schemes, R6, and ASME Section XI in-service flaw assessment criteria tended to underpredict the measured bending moments except for the NSC analysis of the A106B pipe.</p> <p>The effects of flaw geometry and loading history on toughness were evaluated by calculating the toughness from the pipe tests and comparing these results to C(T) values. These effects were found to be variable. The surface-crack geometry tended to increase the toughness (relative to C(T) results), whereas a negative load-ratio significantly decreased the TP304 stainless steel surface-cracked pipe apparent toughness. The inertial experiments tended to achieve complete failure within a few cycles after reaching maximum load in these relatively small diameter pipe experiments. Hence, a load-controlled fracture mechanics analysis may be more appropriate than a displacement-controlled analysis for these tests.</p>					
11. KEY WORDS/DESCRIPTORS (List words or phrases that will assist researchers in locating the report.) Pipe, fracture mechanics, inertial stresses, J-integral, cyclic loading, dynamic loading, circumferential cracks, stainless steel, A106 Grade B.				13. AVAILABILITY STATEMENT Unlimited	
				14. SECURITY CLASSIFICATION (This Page) Unclassified (This Report)	
				Unclassified (This Report)	
				15. NUMBER OF PAGES	
				16. PRICE	

**DATE
FILMED**

10/6/94

END