Deterministic and Probabilistic Evaluations for Uncertainty in Pipe Fracture Parameters in Leak-Before-Break and In-Service Flaw Evaluations

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Battelle

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ABSTRACT

This report presents new results from deterministic and probabilistic analyses to evaluate the significance of a number of technical aspects that may affect LBB or in-service flaw evaluations. In most cases there are both deterministic and probabilistic results. The deterministic analyses were conducted independently of the probabilistic analyses, which offered the opportunity to validate conclusions from each of these independent studies. The technical aspects evaluated relative to LBB uncertainties were: (1) Evaluation of different crack morphology default values, (2) evaluation of COD dependent and independent crack morphology models for tight crack leak-rate analyses, (3) changes of normal operating and N+SSE stress levels on conditional failure probability, (4) dynamic and cyclic loads history effects on load-carrying capacity of through-wall-cracked pipe, (5) evaluation of the effect of off-centered cracks, (6) evaluation of the effect of restraint of pressure induced bending, and (7) evaluation of the effect of residual stresses on leak-rate analyses. Uncertainty analyses conducted relative to in-service flaw evaluations were: (1) dynamic and cyclic load history effects on load-carrying capacity of surface-cracked pipe, and (2) effect of uncertainty in UT flaw sizing. The relative ranking of importance is given for the significance of each technical aspect investigated.

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EXECUTIVE SUMMARY

This report presents new results from deterministic and probabilistic analyses to evaluate the significance of a number of technical aspects that may affect LBB or in-service flaw evaluations. The following summarizes the objectives and results from both the deterministic and probabilistic studies. The reasons for including each technical aspect being evaluated are given first. Then a table is given that summarizes the relative significance of each technical aspect. In most cases there are both deterministic and probabilistic results. The deterministic analyses were conducted independently of the probabilistic analysis, which offered the opportunity to validate conclusions from each of these studies.

Uncertainty Analyses Relative to LBB

Evaluation of Different Crack Morphology Default Values

The objective in this effort was to assess any differences in default crack morphology parameters between PICEP Version 4.0, SQUIRT Version 2.3, and SQUIRT Version 2.4. This evaluation was done only in a deterministic manner. The SQUIRT Version 2.3 default values were typical values from an experiment, but no special attempt was made to define these values. The basis of the PICEP default values is not well documented. It is believed that the IGSCC values came from EPRI/Battelle leak-rate tests, but the default corrosion-fatigue values could not be traced. The SQUIRT 2.4 default crack morphology parameters are the mean values from statistical evaluations of cracks removed from service as determined in NUREG/CR-6004. The crack morphology parameters in these analyses are the surface roughness and the number of turns per unit of thickness. Cracks from both IGSCC and corrosion-fatigue mechanisms were considered.

Evaluation of COD-Dependent and COD-Independent Crack Morphology Models for Tight Crack Leak Rate Analyses

This analysis assessed the effect of using a COD-dependent crack morphology model to alleviate problems in leak-rate calculations for tight cracks. Typical leak-rate codes assume that crack morphology is COD independent. This analysis was only conducted in a deterministic manner.

Changes of Normal Operating and N+SSE Stress Levels on Failure Probability

The objective of this study was to expand the original results from NUREG/CR-6004 to a larger range of normal operating stresses as well as a larger range of N+SSE stresses. This was only a probabilistic study, where the change in conditional failure probabilities was determined over a large range of normal and SSE stresses.

Dynamic and Cyclic Loads History Effect on Load-Carrying Capacity of Through-Wall-Cracked Pipe

This effort assessed the significance of toughness changes due to cyclic and dynamic load effects on LBB load predictions. Both deterministic and probabilistic analyses were conducted on this technical aspect.

Evaluation of the Effect of Off-Centered Cracks

The objective of this investigation was to assess the effect of having a leaking crack off center at normal operating conditions, but centered for N+SSE loading. These analyses were conducted in both deterministic and probabilistic evaluations.

Evaluation of the Effect of Restraint of Pressure Induced Bending

In this evaluation, the effect of the restraint of induced bending from axial stresses in a pipe system was assessed. Typical fracture mechanics analyses assume that the end of a pipe under pressure loading is free to rotate. However, in a pipe system the attached piping restrains this rotation. This restraint of pipe rotation at the crack plane has two effects. First, it increases the maximum load-carrying capacity; second, it reduces the crack-opening displacement for leak-rate analyses. The two effects are in opposite directions when conducting an LBB analysis. These two effects were incorporated in both the deterministic and probabilistic analyses that were conducted.

Evaluation of the Effect of Residual Stresses on Leak-Rate Analyses

The objective of this analysis was to assess the effects of weld residual stresses on leak-rate analyses for LBB evaluations. Deterministic and probabilistic analyses were conducted on this topic. Past work on as-welded pipe and solution-annealed welds with identical cracks showed that using solution annealing to eliminate residual stresses did not improve the fracture loads.

Uncertainty Analyses Relative to In-Service Flaw Evaluations

Dynamic and Cyclic Loads History Effects on Load-Carrying Capacity of Surface-Cracked Pipe

The objective of this effort was to assess the significance of toughness changes due to cyclic and dynamic load effects on surface-cracked pipe predictions for in-service flaw evaluation. Deterministic and statistical analyses were conducted on this technical aspect.

Effect of Uncertainty in UT Flaw Sizing

This analysis was conducted to assess the sensitivity of UT flaw sizing variability on the maximum-load capacity of surface-cracked pipe. The UT flaw sizing variability data came from 1989 EPRI round-robin results on stainless steel piping. Hence, these analyses were conducted only for a stainless steel pipe case.

Ranking of Significance of Various Factors Investigated

The significance of the various technical aspects investigated was ranked in importance of how these technical aspects may change results relative to current analysis methodologies. Table 1 makes this ranking from both the deterministic and probabilistic results for technical aspects that affect LBB evaluations. Table 2 makes this ranking for technical aspects that affect in-service flaw evaluations.

Table 1 Ranking of various technical aspects investigated that affect LBB evaluations from uncertainty study

Technical Aspect Investigated	Comments	Significance
Effect of restraint of pressure induced bending on LBB	Small diameter	Very High
Effect of residual stresses on leak-rate analyses for LBB	Thin-walled pipe (i.e., tension-to-compression stresses through the thickness) at <i>low</i> operating stresses	High to very high
LBB failure probability	Low normal operating stresses	High
COD-dependent crack morphology model leak-rate analyses	Leak rate < 3.5 l/min (0.925 gpm)	High
Effect of off-centered cracks for LBB	Cracks off centered > 50 degrees	Medium to high
LBB failure probability	High N+SSE stresses	Medium
COD-dependent crack morphology model for leak-rate analyses	3.5 l/min (0.925 gpm) < leak rate < 4.5 l/min (1.2 gpm)	Medium
Effect of off-centered cracks for LBB	Statistically varying off-center angle in probabilistic analyses	Medium
Crack morphology default values	For corrosion fatigue cracks	Medium
Effect of residual stresses on leak-rate analyses for LBB	Thin-walled pipe at <i>high</i> normal operating stresses	Low
Crack morphology default values	For IGSCC	Low
COD-dependent crack morphology models for leak-rate analyses	Leak rates > 7.56 l/min (2 gpm)	Low
Dynamic/cyclic history effect on toughness for LBB analyses	Experimental results show analyses too conservative	Low
Evaluation of the effect of off-centered cracks for LBB evaluations	Cracks off centered < 50 degrees	Low
Effect of restraint of pressure induced bending on LBB	Large diameter	Very low
Effect of residual stresses on leak-rate analyses for LBB	Thick-walled pipe at typical operating stresses	Very Low

Table 2 Ranking of various technical aspects investigated that affect in-service flaw evaluations from uncertainty study

Technical Aspect Investigated	Comments	Significance
Uncertainty in UT flaw sizing on failure loads from in-service flaw evaluations	For $a/t > 0.5$	High
Dynamic/cyclic history effect on toughness for surface-cracked pipe (in-service flaw evaluations)	Low yield-to-ultimate strength materials	Medium
Uncertainty in UT flaw sizing on failure loads from in-service flaw evaluations	For $0.25 < a/t < 0.5$	Medium
Dynamic/cyclic history effect on toughness for surface-cracked pipe (in-service flaw evaluations)	High yield-to-ultimate strength materials	Low
Uncertainty in UT flaw sizing on failure loads from in-service flaw evaluations	For $a/t < 0.25$	Low

These rankings are in the categories of very high, high, medium, low, and very low. An example of a very high ranking is the case of restraint of pressure induced bending on LBB, where for small diameter pipe the difference in the failure loads was a factor of 9 which was much greater than the maximum load safety factor of $\sqrt{2}$ used in LBB analyses. An example of the very low rating is the case of residual stresses on leak-rate analyses for a thick-walled pipe. Here the effects were essentially negligible, except at extremely low operating stresses thought to be lower than practical.

A concluding remark is that in some cases it appeared from an initial review that the significance of the deterministic and probabilistic analyses did not agree. Upon further review, it was found that most of the deterministic and probabilistic analyses agreed on the significance of a technical aspect, but there were a few cases that did not agree. The cases where there were differences in the significance of a technical aspect were found to be due to: (1) the deterministic model being more sophisticated than that used in the probabilistic analysis, (2) the material property input not being the same in the deterministic and probabilistic analyses, and (3) what appeared to be a significant effect over a small range was thought to be less important from a deterministic view, but that same factor gave a major change in conditional failure probabilities. All of these differences were closely evaluated to make our final ranking.

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NOMENCLATURE

1. Symbols

a	Surface crack depth
C	Power-law coefficient for modeling J-resistance curve
C_1, C_2	Constants in the friction factor correlation
c	Half crack length
D_H	Hydraulic diameter
D_o	Outside diameter of pipe
E	Modulus of elasticity
F	Parameter in original Ramberg-Osgood equation
F_{dyn}	Ratio of dynamic J_{Ic} to quasi-static J_{Ic}
F_{cyc}	Ratio of cyclic J_{Ic} (for $R = -1$ loading) to quasi-static J_{Ic}
F_{tot}	$F_{dyn} * F_{cyc}$
f	Friction factor
$g(x)$	Performance function in x space
I	Moment of inertia of uncracked pipe cross-section
J	J-integral (energy release rate)
J_{cyc}	J value under cyclic loading
J_{dyn}	J value under dynamic loading
J_{Ic}	Plane strain J at crack initiation by ASTM E813
J_{QS}	J value under quasi-static monotonic loading
$J_{seismic}$	J value under combined cyclic and dynamic loading
J_R	J-resistance (toughness)
K_G	Correction factor for global path deviation
K_l	Correction factor for local path deviation
k	Surface roughness
k_g	Global surface roughness
k_l	Local surface roughness
M	Bending moment

Nomenclature

M_{baseline}	Baseline moment using actual flaw size
M_{max}	Maximum moment-carrying capacity of pipe
$M_{\text{N+SSE}}$	Applied moment from normal plus safe shutdown earthquake stresses
M_{UT}	Moment calculated using UT flaw size
m	Power-law exponent for modeling J-resistance curve
N_G	Global number of 90-degree turns
N_l	Local number of 90-degree turns
n	Strain-hardening exponent in Ramberg-Osgood model
Pr	Probability operator
P_F	Conditional probability of failure
p	Internal pressure
R	Radius
R_m	Mean radius
r	A dimensional parameter with a value of unity
S_m	Code-specified nominal design stress
S_y	Yield strength
t	Pipe wall thickness
α	Coefficient of Ramberg-Osgood equation
Δa	Crack extension used in modeling J-resistance curve
δ	Center-crack-opening displacement
δ_M	Center-crack-opening displacement without residual stress (moment loading only)
δ_{M+RS}	Center-crack-opening displacement with residual stress and moment loading
ϵ	Total strain
ϵ_0	Reference strain in Ramberg-Osgood model
θ	Half crack angle
σ	Stress
σ_N	Normal operating stress
$\sigma_{\text{N+SSE}}$	Stress at normal plus safe shutdown earthquake conditions
$\sigma_{\text{SL-A}}$	ASME Service Level A stress limit
σ_b	Bending stress
σ_f	Flow stress

σ_t	Axial tension stress
σ_y	Yield strength
σ_u	Ultimate strength
σ_o	Reference stress in Ramberg-Osgood model

2. Acronyms and Initialisms

ASME	American Society of Mechanical Engineers
ASTM	American Society for Testing Materials
BWR	Boiling water reactor
COD	Crack-opening displacement
COV	Coefficient of variation
CS	Carbon steel
C(T)	Compact (tension) specimen
CPOF	Conditional Probability of Failure
EPFM	Elastic-plastic fracture mechanics
EPRI	Electric Power Research Institute (U.S.)
FEM	Finite element method
FORM	First-order reliability method
GE	General Electric (U.S.)
IGSCC	Intergranular stress corrosion cracking
IPIRG-1	First International Piping Integrity Research Group
IPIRG-2	Second International Piping Integrity Research Group
J-R	J-resistance
LBB	Leak-before-break
L-C	Longitudinal-circumferential (orientation of fracture specimens)
LEFM	Linear-elastic fracture mechanics
L-R	Longitudinal-radial (orientation of fracture specimens)
NDE	Non-destructive evaluation
NRC	Nuclear Regulatory Commission (U.S.)
NSC	Net-Section-Collapse (analysis)
N+SSE	Normal plus safe shutdown earthquake

Nomenclature

PICEP	Pipe Crack Evaluation Program (computer code)
PIFRAC	Pipe FRACTure (Nuclear pipe material property database)
PROLBB	PRObabilistic Leak-Before-Break (computer code)
PSQUIRT	Probabilistic Seepage Quantification of Upsets In Reactor Tubes (computer code)
PWR	Pressurized water reactor
QS	Quasi-static
SAW	Submerged-arc weld
SMAW	Shielded-metal-arc weld
SQUIRT	Seepage Quantification of Upsets in Reactor Tubes (computer code)
SORM	Second-order reliability method
SS	Stainless steel
SSE	Safe-shutdown earthquake
TWC	Through-wall-cracked, through-wall crack
UT	Ultrasonic testing

PREVIOUS REPORTS IN SERIES

Previous Reports from this Program

"Summary of Results from the IPIRG-2 Round-Robin Analyses," NUREG/CR-6337, BMI-2186, February 1996.

"The Effect of Cyclic and Dynamic Loads on Carbon Steel Pipe," NUREG/CR-6438, BMI-2188, February 1996.

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"Development of a J-Estimation Scheme for Internal Circumferential and Axial Surface Cracks in Elbows," NUREG/CR-6445, BMI-2193, June 1996.

Reports from the IPIRG-1 Program

"Evaluation and Refinement of Leak-Rate Estimation Models," NUREG/CR-5128, Revision 1, June 1994.

"Loading Rate Effects on Strength and Fracture Toughness of Pipe Steels Used in Task 1 of the IPIRG Program," Topical Report, NUREG/CR-6098, October 1993.

"Stability of Cracked Pipe Under Inertial Stresses," NUREG/CR-6233, Vol. 1, August 1994.

Previous Related Documents from NRC's Short Cracks in Piping and Piping Welds Program

"Short Cracks in Piping and Piping Welds," First Semiannual Report, NUREG/CR-4599, Vol. 1, No. 1, March 1991.

"Short Cracks in Piping and Piping Welds," Second Semiannual Report, NUREG/CR-4599, Vol. 1, No. 2, April 1992.

"Short Cracks in Piping and Piping Welds," Third Semiannual Report, NUREG/CR-4599, Vol. 2, No. 1, September 1992.

"Short Cracks in Piping and Piping Welds," Fourth Semiannual Report, NUREG/CR-4599, Vol. 2, No. 2, February 1993.

"Short Cracks in Piping and Piping Welds," Fifth Semiannual Report, NUREG/CR-4599, Vol. 3, No. 1, October 1993.

Previous Reports in Series

"Short Cracks in Piping and Piping Welds," Sixth Semiannual Report, NUREG/CR-4599, Vol. 3, No. 2, March 1994.

"Short Cracks in Piping and Piping Welds," Progress Report, NUREG/CR-4599, BMI-2173, Vol. 4, No. 1, April 1995.

"Fracture Behavior of Short Circumferential Short-Surface-Cracked Pipe," NUREG/CR-6298, November 1995.

"Fracture Evaluations of Fusion Line Cracks in Nuclear Pipe Bimetallic Welds," NUREG/CR-6297, April 1995.

"Effect of Dynamic Strain Aging on the Strength and Toughness of Nuclear Ferritic Piping at LWR Temperatures," NUREG/CR-6226, October 1994.

"Effects of Toughness Anisotropy and Combined Loading on Fracture Behavior of Ferritic Nuclear Pipe," NUREG/CR-6299, April 1995.

"Refinement and Evaluation of Crack-Opening Analyses for Circumferential Through-Wall Cracks in Pipes," NUREG/CR-6300, April 1995.

"Probabilistic Pipe Fracture Evaluations for Leak-Rate Detection Applications," NUREG/CR-6004, April 1995.

"Stainless Steel Submerged Arc Weld Fusion Line Toughness," NUREG/CR-6251, April 1995.

"Validity Limits in J-Resistance Curve Determination: Volume 1: An Assessment of the J_M Parameter," NUREG/CR-6264, Volume 1, February 1995.

"Validity Limits in J-Resistance Curve Determinations: Volume 2: A Computational Approach to Ductile Crack Growth Under Large-Scale Yielding Condition," NUREG/CR-6264, Volume 2, February 1995.

Previous Related Documents from NRC's Degraded Piping Program - Phase I Reports

"The Development of a Plan for the Assessment of Degraded Nuclear Piping by Experimentation and Tearing Instability Fracture Mechanics Analysis," NUREG/CR-3142, Vols. 1 and 2, June 1983.

Previous Related Documents from NRC's Degraded Piping Program - Phase II Reports

"Degraded Piping Program - Phase II," Semiannual Report, NUREG/CR-4082, Vol. 1, Oct. 1984.

"Degraded Piping Program - Phase II," Semiannual Report, NUREG/CR-4082, Vol. 2, June 1985.

"Degraded Piping Program - Phase II," Semiannual Report, NUREG/CR-4082, Vol. 3, March 1986.

"Degraded Piping Program - Phase II," Semiannual Report, NUREG/CR-4082, Vol. 4, July 1986.

"Degraded Piping Program - Phase II," Semiannual Report, NUREG/CR-4082, Vol. 5, Dec. 1986.

"Degraded Piping Program - Phase II," Semiannual Report, NUREG/CR-4082, Vol. 6, April 1988.

"Degraded Piping Program - Phase II," Semiannual Report, NUREG/CR-4082, Vol. 7, March 1989.

"Degraded Piping Program - Phase II," Semiannual Report, NUREG/CR-4082, Vol. 8, March 1989.

"NRC Leak-Before-Break (LBB/NRC) Analysis Method for Circumferentially Through-Wall Cracked Pipes Under Axial Plus Bending Loads," Topical Report, NUREG/CR-4572, March 1986.

"Elastic-Plastic Finite Element Analysis of Crack Growth in Large Compact Tension and Circumferentially Through-Wall-Cracked Pipe Specimen--Results of the First Battelle/NRC Analysis Round Robin," Topical Report, NUREG/CR-4573, September 1986.

"An Experimental and Analytical Assessment of Circumferential Through-Wall Cracked Pipes Under Pure Bending," Topical Report, NUREG/CR-4574, June 1986.

"Predictions of J-R Curves With Large Crack Growth From Small Specimen Data," Topical Report, NUREG/CR-4687, September 1986.

"An Assessment of Circumferentially Complex-Cracked Pipe Subjected to Bending," Topical Report, NUREG/CR-4687, September 1986.

"Analysis of Cracks in Stainless Steel TIG Welds," Topical Report, NUREG/CR-4806, November 1986.

"Approximate Methods for Fracture Analyses of Through-Wall Cracked Pipes," Topical Report, NUREG/CR-4853, January 1987.

"Assessment of Design Basis for Load-Carrying Capacity of Weld-Overlay Repair," Topical Report, NUREG/CR-4877, February 1987.

"Analysis of Experiments on Stainless Steel Flux Welds," Topical Report, NUREG/CR-4878, February 1987.

"Experimental and Analytical Assessment of Circumferentially Surface-Cracked Pipes Under Bending," Topical Report, NUREG/CR-4872, April 1987.

Previous Reports in Series

Other Related Program Reports

“Validation of Analysis Methods for Assessing Flawed Piping Subjected to Dynamic Loading,”
NUREG/CR-6234, August 1994.

1.0 INTRODUCTION

During the NRC's Degraded Piping Program (Ref. 1.1), Short Cracks in Piping and Piping Welds program (Ref. 1.2), and International Piping Integrity Research Group programs (Ref. 1.3), numerous factors were uncovered that could affect leak-before-break (LBB) and in-service flaw evaluation criteria. The work reported here explored the significance of these technical aspects which currently are not considered in pipe fracture analyses.

LBB analyses conducted using the U.S. NRC's Draft Standard Review Plan 3.6.3 (Ref. 1.4), basically use the following procedure:

- (1) If a piping system has no history of cracking, and there is no history of large unquantifiable loads (i.e., water-hammer loads), then the piping system is a candidate for LBB.
- (2) For a candidate pipe system to be accepted for LBB, it must be shown that:
 - (i) At normal operating conditions, there is a hypothetical leaking crack size (with some safety factor on the leak rate) that can be detected with plant leak-detection systems, and
 - (ii) For that hypothetical crack size, the pipe system is stable at normal plus safe-shutdown earthquake (N+SSE) loads. There is another safety factor on either the crack size or the N+SSE loads in this step. This relatively simple analysis involves considering the worst case fracture toughness in a pipe location with the highest stresses.

A pragmatic translation of this criteria is: (1) if your pipe system has no cracks and low loads, you should have LBB, and (2) even though your pipe system should never have a crack, prove that a leak-detectable crack would be stable using a relatively simple fracture mechanics procedure.

In more recent years, several organizations have considered applying LBB to pipe systems where there is a possibility of some cracking. Additionally, the question about what happens if a system approved for LBB suddenly develops a cracking problem needs to be resolved, i.e., the recent German BWR TP347 pipe cracking problem (Ref. 1.5). To address LBB applicability to the potential of real cracking, which is not allowed in the NRC Draft Standard Review Plan 3.6.3 procedure, draws in additional parameters.

Examples of some of these technical aspects are:

- (a) The Draft Standard Review Plan 3.6.3 procedure requires use of the worst case material toughness. Currently, quasi-static strength and toughness values are used. A question raised at the beginning of the IPIRG-1 program was, how do dynamic and cyclic load histories from a seismic event affect the material toughness? Dynamic strain aging can reduce the toughness of some ferritic steels at "seismic" loading rates, but increase the toughness of austenitic steels, see Ref. 1.6. Reverse cyclic loading during ductile tearing was found to reduce the toughness of both ferritic and austenitic steels, see Refs 1.7 through 1.10.

- (b) What is the sensitivity of leakage crack sizes using different crack morphology parameters? Service cracks are much rougher than fatigue cracks grown in air (Ref. 1.11), yet most of the leak-rate analyses are based on laboratory specimen data.
- (c) What if the crack did not start on the bending plane of the normal operating stresses due to randomness of welding imperfections? Currently it is assumed that the crack is centered on the normal operating bending plane, and the SSE stresses are in the same bending plane.
- (d) Are there boundary conditions in a pipe system not properly represented by leak-rate and fracture analyses? For example, pressure stresses cause a cracked pipe to rotate due to the eccentricity created by the crack. The magnitude of the rotation depends on the crack size relative to the pipe diameter and the crack location in the pipe system, see Ref. 1.12. Leak-rate and fracture analyses in existing LBB procedures assume that the pipe can freely rotate due to the pressure stresses, but the pipe system may restrain this rotation. Pipe system fracture experimental results from the IPIRG-1 program showed that the restraint of bending is beneficial for fracture stability (Ref. 1.13) which is beneficial for LBB, but for leakage analyses considerations, the restraint of bending lowers the crack-opening displacement (Ref. 1.12) which is detrimental for LBB. How do these two counteracting trends affect an LBB analysis?
- (e) Can weld residual stresses affect LBB? The NRC Draft Standard Review Plan 3.6.3 says they that should be considered, but how? Past full-scale pipe fracture test data show that they may not affect fracture behavior for even a low-toughness nuclear pipe material condition (Ref. 1.14), but what about leak-rate analyses?
- (f) In LBB analyses, what is more significant, knowing the normal operating stresses well, or knowing the SSE stresses well? NRC Draft Standard Review Plan 3.6.3 says to use the highest stressed location. However, no guideline is given on whether this should be the highest normal operating stress location or the highest N+SSE stress location in Draft Standard Review Plan 3.6.3, although the intent from NUREG-1061 Volume 3 was to use the highest N+SSE stress location. Is there some combination of normal and SSE stresses that will be harder to satisfy LBB than examining only the highest N+SSE stress locations, and what guidance should be given in such a case?

For in-service flaw evaluations, technical aspects that have not been addressed include:

- (a) How does the statistical variability of material parameters compare with the applied safety factors on loads?
- (b) Do dynamic and cyclic load histories on toughness significantly affect allowable loads? Like LBB analyses, the ASME and R6 flaw evaluation criteria considers the elastic design SSE stress and uses that peak stress in an elastic-plastic fracture mechanics analysis with toughness values from quasi-static monotonically loaded fracture specimens. The effects of dynamic/cyclic loading on toughness during the seismic event are not considered. Should there be different toughness considerations for normal operating conditions than SSE or faulted conditions?

- (c) How do uncertainties in flaw sizing accuracy affect failure load predictions? Currently, uncertainty in flaw sizing is not addressed in pipe flaw evaluation criteria. If they account for the uncertainty in the depth measurements, how do they do that? Can guidance from fracture calculations be of help?

To determine the significance of these technical aspects, several deterministic and probabilistic calculations were undertaken as part of the Second International Piping Integrity Research Group (IPIRG-2) program. The probabilistic calculations were an extension of the efforts in Reference 1.11, whereas the deterministic uncertainty analyses were conducted independently. In cases where both probabilistic and deterministic analyses were conducted, the independent nature of these analyses efforts offered the opportunity to check the significance of each analysis against the other. If discrepancies were found in the prediction of the significance of that technical aspect, then the reason for that discrepancy was explored and accounted for in determining the overall significance of that technical aspect.

It should also be recognized that this was a limited study to determine if these factors should be examined further. Additionally, there are technical aspects that were not considered in this analysis. Examples of other technical aspects are:

- (a) Low cycle fatigue crack growth is not currently considered in LBB or in-service flaw evaluation for SSE loading. This may be due to the fact that ASME Section III pipe design criteria do not require a fatigue evaluation for Service Level C or D loads. However, a seismic event may have 5 to 30 cycles. In Reference 1.15, it was demonstrated that a classical LEFM fatigue crack growth analysis will drastically underpredict low-cycle fatigue crack growth. It was also shown that an elastic-plastic ΔJ method was found to predict experimental results well when proper care was taken to account for crack-closure loads.
- (b) The pipe system stresses are elastically calculated. At high loads, there would be plasticity not only at the crack location, but perhaps in uncracked pipe locations. This plasticity may affect the magnitude of the seismic loads significantly. In an example plant calculation, the seismic moments were found to be reduced by a factor of five when modeling the crack plasticity in a seismic time-history analysis (Refs 1.16 and 1.17). This significant effect may counteract any negative aspects previously described.
- (c) At a nozzle or other thickness transition location, the crack opening will not be symmetric, see Ref. 1.12. Hence, the resulting crack length for a given leak rate would be longer if the pipe thickness was used at a nozzle girth weld location.

Such calculations were beyond the scope of this study. Nevertheless, this report presents a major leap in understanding the significance of technical aspects not previously considered in pipe flaw analyses.

1.1 References

- 1.1 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 Columbus Division, NUREG/CR-4082, Vol. 8, March 1989.

- 1.2 Wilkowski, G., Krishnaswamy, P., Brust, F., Francini, R., Ghadiali, N., Kilinski, T., Marschall, C., Mohan, R., Rahman, S., Rosenfield, A., Rudland, D., Scott, P., "Key Results for the NRC's Short Cracks in Piping and Piping Welds Research Program," NUREG/CP-0139, October 1994.
- 1.3 Wilkowski, G., M., Olson, R., Scott, P., Brust, F., Ghadiali, N., Kilinski, T., Rudland, D., Hopper, A., "The IPIRG Programs - Advances in Pipe Fracture Technology," *Proceedings of LBB'95 Conference*, Lyon France, October 1995.
- 1.4 Published for public comment on "Standard Review Plan, Section 3.6.3 Leak-Before-Break Evaluation Procedures," *Federal Register*, Vol. 52, No. 167, Notices, pp 32626 to 32633, Friday, August 28, 1987.
- 1.5 Erve, M., Wesseling, U., Kilian, R., Hardt, R., Brummer, G., Maier, V., and Ilg, U., "Cracking in Stabilized Austenitic Steel Piping of German Boiling Water Reactors - Characteristic Features and Root Causes" in *Proceedings of 20th MPA-Seminar- Approaches to Lifetime Extension of Nuclear Power Plants*, Vol. 2, Paper 29, October 1994, (in German).
- 1.6 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.
- 1.7 Landes, J. and Liaw, P. K., "Effect of Load-History on Fracture Behavior," in *Advances in Fracture and Fatigue for the 1990's - Volume 1 - Load History Effects on Fracture Resistance*, ASME PVP Vol. 166, pp 45-54, July 1989.
- 1.8 Scott, P., Kramer, G., Vieth, 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, pp 207-220, June 1994.
- 1.9 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, pp 221-240, June 1994.
- 1.10 Olson, R., Scott, P., Marschall, C., and Wilkowski, G., "Comparison of Fracture Toughness Values from an IPIRG-1 Large-Scale Pipe System Test and C(T) Specimens on Wrought TP304 Stainless Steel," ASME PVP Vol. 280, pp 241-254, June 1994.
- 1.11 Rahman, S., Ghadiali, N., Paul, D., and Wilkowski, G., "Probabilistic Pipe Fracture Evaluations for Leak-Rate Detection Applications," NUREG/CR-6004, April 1995.
- 1.12 Rahman, S., Brust, F., Ghadiali, N., Choi, Y. H., Krishnaswamy, P., Moberg, F., Brickstad, B., and Wilkowski, G., "Refinement and Evaluation of Crack-Opening Analyses for Short Circumferential Through-Wall Cracks in Pipes," NUREG/CR-6300, April 1995.
- 1.13 Scott, P., Olson, R., and Wilkowski, G., "The IPIRG-1 Pipe System Fracture Tests -- Experimental Results," ASME PVP Vol. 280, pp 135-152, June 1994.

- 1.14 Wilkowski, G. M., and others, "Analysis of Experiments on Stainless Steel Flux Welds," NUREG/CR-4878, April 1987.
- 1.15 Wilkowski, G., Rahman, S., and Mohan, R., "Low-Cycle Fatigue Crack Growth Considerations in Pipe Fracture Analyses," ASME PVP Vol. 280, June 1994, pp 281-298.
- 1.16 Olson, R., Wolterman, R., and Wilkowski, G., "Margins for Dynamic FEM Analysis of Cracked Pipe Under Seismic Loading for the DOE New Production Reactor," ASME PVP Vol. 280, pp 119-134, June 1994.
- 1.17 Olson, R., Wolterman, R., Scott, P., Krishnaswamy, P., and Wilkowski, G., "The Next Generation Methodology for Cracked Pipe System Subjected to Dynamic Loads," ASME PVP Vol. 275-1, pp 159-172, June 1994.

2.0 OBJECTIVE

The objective of the LBB uncertainty study was to determine the effects of various parameters on their significance in LBB and in-service flaw evaluations.

Section 3.0 gives the results from the deterministic analyses for both the LBB and in-service applications for the following parameters:

1. Variation of load-carrying capacity of cracked pipe due to effect of cyclic and dynamic loading on fracture toughness for both in-service flaw evaluation considerations (surface flaws) and LBB (through-wall flaws),
2. Sensitivity study of leakage flaw sizes using different crack morphology parameters,
3. Effects of off-centered cracks during leakage on the maximum load-carrying capacity for LBB evaluation,
4. Effect of restraint of pressure-induced bending in a piping system on LBB, and
5. Effect of weld residual stresses on LBB analysis.

Section 4.0 gives the results from probabilistic analyses on the following parameters for LBB applications:

1. Magnitude of normal operating stresses,
2. Increased normal plus safe-shutdown earthquake (N+SSE) stresses addressing proposed changes to the ASME Section III design stresses,
3. Off-centered leaking cracks,
4. Restraint of pressure-induced bending,
5. Dynamic and cyclic load effects on fracture toughness, and
6. Weld residual stresses.

Section 5.0 describes the results of the in-service flaw evaluation uncertainty study. The effects of the following parameters on the statistical distribution of failure loads were determined:

1. Quasi-static material properties were statistically varied using fixed flaw sizes for baseline calculations of maximum moments,
2. Using the same flaw sizes, the effect of dynamic and cyclic loads on material toughness, the resulting statistical variation on maximum moments was determined, and

3. Again using the same average flaw sizes with the statistically varied quasi-static material properties, the flaw depth was then statistically varied to account for uncertainty in crack depth measurements.

Section 6.0 gives the summary and conclusions from this study.

The above objectives for the uncertainty analyses were accomplished by modifying and upgrading various methods and software developed in the NRC's Short Cracks in Piping and Piping Welds Program (Ref. 2.1). Also, data generated in that program were used to achieve the objectives.

2.1 References

- 2.1 Rahman, S., Ghadiali, N., Paul, D., and Wilkowski, G., "Probabilistic Pipe Fracture Evaluations for Leak-Rate Detection Applications," NUREG/CR-6004, April 1995.

3.0 DETERMINISTIC EVALUATIONS

The following factors were investigated in a deterministic manner:

- Variation of load-carrying capacity of cracked pipe due to effect of cyclic and dynamic loading on fracture toughness for both in-service flaw evaluation considerations (surface flaws) and LBB (through-wall flaws),
- Sensitivity study of leakage flaw sizes using different crack morphology parameters,
- Effects of off-centered cracks during leakage on the maximum load carrying capacity for LBB evaluation,
- Effect of restraint of pressure-induced bending in a piping system on LBB, and
- Effect of weld residual stresses on LBB analysis.

3.1 Evaluation of the Change in Load-Carrying Capacity of Surface-Cracked Pipes (For In-Service Flaw Evaluation Criteria) Due to Effects of Dynamic and Cyclic Loading on Toughness

One of the aspects evaluated during the course of the IPIRG programs was the effect of dynamic and cyclic loading history on the fracture toughness of nuclear piping steels (Ref. 3.1). Using the trend curves of dynamic loading and cyclic effects on toughness given in Figure 3.1 (Ref. 3.1), the change in load-carrying capacity was calculated for surface-cracked pipe. For this analysis, we assumed that the effects of dynamic and cyclic loading on toughness could be calculated from the following equation.

$$J_{\text{seismic}} = F_{\text{dyn}} F_{\text{cyc}} J_{\text{QS}} \quad (3-1)$$

where

- J_{seismic} = J value under dynamic and cyclic loading,
- F_{dyn} = Ratio of dynamic J_{Ic} to quasi-static J_{Ic} ,
- F_{cyc} = Ratio of cyclic J_{Ic} (for $R = -1$ loading) to quasi-static J_{Ic} , and
- J_{QS} = J value under quasi-static monotonic loading (i.e., typical ASTM value).

Calculations were made for TP304 stainless steel base metal to see what effects there may be for a high toughness material. Pipe DP2-A23 was close to ASME Section II strength values at 288 C (550 F), and hence its properties were used in these calculations. For this pipe with a quasi-static σ_y/σ_u of 0.31, F_{dyn} was 1.0 and F_{cyc} was 0.38 for fully reversed ($R = -1$) loading. Values for different R-ratios were not available at the time of this study. Hence, for the dynamic/cyclic loading conditions, the J values in the quasi-static monotonic J-R curve were multiplied by 0.38.

Figure 3.1a Effect of dynamic loading on toughness

Figure 3.1b Effect of cycle loading on toughness

Sample calculations were made using the SC.TNP1 J-estimation scheme which was the most accurate of all the surface-cracked pipe J-estimation schemes evaluated in the NRC's Short Cracks in Piping and Piping Welds program (Ref. 3.2). The SC.TNP1 analysis predicts the maximum loads accounting for elastic-plastic fracture mechanics. From recent sensitivity studies in Reference 3.2, it was found that the surface-cracked-pipe maximum loads could be normalized by the limit load, and thereby become independent of the effect of surface-crack geometry for a large number of practical flaw sizes. The ratio of the Net-Section-Collapse failure load to the SC.TNP1 maximum load was determined for various pipe sizes from 102-mm (4-inch) to 2.44-m (96-inch) diameter pipe with an R_m/t ratio of 10. (The 2.44 m, [96-inch] diameter cylinder calculations were actually conducted to assess the applicability of the Net-Section-Collapse analysis to BWR core shroud cracks.) The calculations were made using the NRCPIPES Computer Code (Version 2.0a).

Figure 3.2 shows the results of these Net-Section-Collapse to EPFM maximum load-capacity ratios as a function of outside pipe diameter. Interestingly, the results showed that for the quasi-static-monotonic loading, this ratio was below 1.0 for pipe diameters less than 559 mm (22 inches), and was less than 1.1 for pipe diameters less than 1,016 mm (40 inches). This result is consistent with the assumptions that stainless steel nuclear pipe should fail at limit-load conditions in the ASME Section XI criteria. However, for the case of dynamic/cyclic loading, the Net-Section-Collapse to EPFM ratio increased and suggested that the Net-Section-Collapse analysis would significantly overpredict the load-carrying capacity for pipes with diameters larger than 304 mm (12 inches). [Note: when the ratio was larger than 1.1 a significant effect was considered to exist. The basis of the 1.1 value comes from the fact that the reproducibility of pipe fracture tests is typically around 10 percent, hence effects less than 10 percent are considered insignificant.] In general, the ratio of the dynamic/cyclic to the quasi-static monotonic curves in Figure 3.2 showed a 20 to 30 percent drop in load-carrying capacity for surface-cracked stainless steel nuclear pipes under cyclic/dynamic loading.

Figure 3.2 Ratio of NSC to EPFM maximum loads (Z-factor) for surface-cracked stainless steel pipe ($R_m/t = 10$)

Since, in the future, these calculations could have a large effect on pipe flaw evaluation criteria, it was deemed necessary to further validate these results. To do so, existing quasi-static and dynamic/cyclic pipe experiments from past programs were used to determine experimental values for comparison with the calculated curves. This was done by dividing the Net-Section-Collapse analysis maximum load for that experiment by the experimental maximum load. Data existed from 152-mm (6-inch) nominal diameter TP304 pipe used in quasi-static experiments during the Degraded Piping Program (Ref. 3.3) and an inertially loaded surface-cracked pipe experiment in IPIRG-1 (Ref. 3.4). Data also existed for a quasi-static 406.4-mm (16-inch) nominal diameter pipe experiment from a past EPRI program at Battelle (Ref. 3.5), and an IPIRG-1 pipe system experiment (Ref. 3.6). The experimental values are indicated in Figure 3.2. The quasi-static experimental maximum loads agreed very well with the calculated curve. The 152-mm (6-inch) nominal diameter pipe dynamic/cyclic experimental value was below the calculated curve, and was almost on the quasi-static monotonic curve and experimental data point. However, the 406.4-mm (16-inch) nominal diameter pipe experimental value was very close to the calculated cyclic/dynamic curve even though it was at a lower R-ratio of -0.3. This R-ratio is more typical of nuclear piping seismic loads, which gives credibility to that curve.

3.2 Evaluation of the Change in Load-Carrying Capacity of Through-Wall-Cracked Pipe (For LBB) Due to the Effects of Dynamic and Cyclic Loading on Toughness

For LBB analyses, a through-wall-cracked pipe evaluation is used. Past analyses have shown that the ratio of the Net-Section-Collapse (NSC) to EPFM failure load (sometimes referred to as a Z-factor) is sensitive to flaw length, Ref. 3.7, as well as pipe diameter and material toughness. To evaluate such crack length effects, two different sets of analyses were conducted.

In the first set of analyses, the ratio of crack length to pipe circumference was kept constant at 30 percent. A 20 to 30-percent crack length has been found to give the largest ratio between limit-load and EPFM maximum loads, Ref. 3.7. This trend with crack length is illustrated for the LBB.NRC (Ref. 3.8) and original GE/EPRI methods in (Ref. 3.9) Figure 3.3 for a 203.2-mm (8-inch) nominal diameter pipe size and with quasi-static monotonic stainless steel material properties. It can be seen in Figure 3.3 that there is a difference between the two methods. The GE/EPRI method typically gives the greatest underprediction of experimental loads, and the LBB.NRC method typically gives the highest prediction of loads, hence these two analyses will bound the results of the other J-estimation scheme analyses. It is also worthy to note that the U.S. NRC Draft Standard Review Plan 3.6.3 (Ref. 3.10) uses a Z-factor based on crack size to get the maximum Z-factor value. This approach is essentially the same approach employed in the first set of analyses in this effort.

Calculations were subsequently done for stainless steel base metal using both the Original GE/EPRI and the LBB.NRC methods. The NRCPIPE Code (Version 2.0) was used in these calculations. As with the calculations done for surface-cracked pipe in the prior section, J-R curves for quasi-static monotonic loading and for dynamic cyclic loading were used. The ratio of the NSC to the EPFM maximum loads for the GE/EPRI and LBB.NRC methods as a function of pipe diameter are shown in Figure 3.4a. Here it can be seen that the ratio continually increases as the diameter increases, and the ratio is much higher when the GE/EPRI method is used and also when the dynamic-cyclic J-R curve is used. To validate these trends,

Figure 3.3 Ratio of NSC to EPFM maximum loads (Z-factor) as a function of crack length for nominal 8-inch diameter TP304 pipe

experimental data from past through-wall-cracked, stainless steel pipe experiments were used. The ratio of the NSC-to-experimental maximum loads are plotted in Figure 3.4a for 152-mm (6-inch) nominal diameter pipe experiments from the IPIRG-1 program for quasi-static and cyclic loading, Ref. 3.11. The experimental results show the trend of the cyclic pipe experiment having a higher ratio than the monotonic test, but the absolute values are lower than the LBB.NRC values and much lower than the GE/EPRI values. Hence, the effects of cyclic loading were overestimated by the analysis methods.

In the second set of analyses, the crack length was held constant at 152 mm (6 inches). It has been noted that for LBB applications, this crack length is typical for all pipe diameters. As in the prior surface-cracked pipe analyses, these calculations were done for TP304 stainless steel pipes using the quasi-static, monotonic, J-R curve and the dynamic, cyclic ($R = -1$), J-R curve. Figure 3.4b shows the results assuming the crack length is held fixed at 152 mm (6 inches). Here it is interesting to see that the NSC/EPFM ratio reaches a maximum for a 254-mm (10-inch) nominal diameter pipe, and then almost remains constant for pipe larger than 10-inch nominal diameter. The reason for this trend is explained by looking at the results in Figure 3.3. The 254-mm (10-inch) nominal diameter pipe with a 152-mm (6-inch) long crack is 18-percent of the circumference whereas the NSC/EPFM maximum load ratio was the greatest for this pipe size. For pipes smaller or larger than 254-mm (10-inches) in diameter, there is a trade-off with increasing NSC/EPFM ratio against pipe diameter and decreasing NSC/EPFM ratio with different crack lengths. The results in Figure 3.4b show lower NSC/EPFM values than the NSC/EPFM values in Figure 3.4a for all but the 254-mm (10-inch) nominal diameter pipe.

Figure 3.4a Ratio of NSC to EPFM maximum loads (Z-factor) when keeping TWC equal to 30 percent of circumference

Figure 3.4b Ratio of NSC to EPFM maximum loads (Z-factor) when keeping the circumferential TWC length to 152.4 mm, (6 inches), (typical in LBB evaluations)

3.3 Sensitivity Study of Leakage Flaw Sizes Using Different Default Crack Morphology Parameters

During the course of the IPIRG-2 program, numerous round-robin calculations were performed, Ref. 3.12. One aspect that was evaluated further in this program was the effect of crack morphology parameters used in leak-rate analyses for LBB. In Reference 3.13, a probabilistic leak-rate analysis was done, and in doing so it was determined that there was not a good technical basis for crack morphology parameters used in leak-rate analysis. A statistical data base was developed for cracks removed from service and was reported in Reference 3.13. When the average values were used in one of the IPIRG-2 round-robin problems, a large difference between leak rates calculated using these average values were found when compared to leak rates calculations by other participants (Ref. 3.12). These differences come from the fact that service cracks due to corrosion-fatigue or IGSCC are much rougher than cracks grown without environmental effects, see Figure 3.5. To assess the significance of different default crack morphology values used, the following calculations were conducted.

Variation of Default Crack Morphology Parameters. The first set of sensitivity calculations (Set A) involved using default roughness and number of turn values from SQUIRT Version 2.3, PICEP Version 4.0, and the average values from Reference 1 as noted below in Table 3.1.

Table 3.1 Summary of different crack morphology variables for Set A leak-rate analyses

	Surface Roughness, μm (μin)		Number of 90-degree turns, $\#/\text{mm}$ ($\#/\text{in}$)	
	IGSCC	Corrosion-Fatigue	IGSCC	Corrosion-Fatigue
SQUIRT (V2.3)	62.0 (2,440)	40.0 (1,570)	3.00 (76.2)	6.00 (152)
PICEP (V4.0)	5.1 (201)	25.0 (984)	1.89 (48.0)	0.00 (0.00)
Reference 2.4	80.0 (3,150)	40.5 (1,590)	2.82 (71.6)	0.67 (17.0)

These calculations were done using the SQUIRT computer code (Version 2.3). The calculations involved assuming:

- Leak-rates of 18.9 liters per minute (5.0 gallons per minute), and 1.89 liters per minute (0.5 gpm),
- Outside pipe diameters of 114, 356, and 813 mm (4.5, 14, and 32 inches) with R_m/t ratios of 10,
- A pressure of 15.51 MPa (2,250 psi) and a temperature of 288 C (550 F), and
- A bending stress to give a total pressure and bending stress of 50 percent of the Service Level A maximum allowable stress from ASME Section III Article NB-3650 for TP304 pipe.

Figure 3.5 Environmental effects on crack morphology

The differences in the resulting IGSCC crack lengths are shown in Figure 3.6a for 1.89 and 18.9 liters per minute (0.5 and 5.0 gpm) leaks. Figure 3.6a shows that the corresponding crack lengths calculated using the SQUIRT and PICEP default values were very close considering the safety factor of 2 on crack length being applied in the U.S. NRC LBB procedure.

The results for the corrosion-fatigue crack are shown in Figure 3.6b. For the corrosion-fatigue crack there was a larger difference of 16 to 40 percent in the crack length for the 18.9 liters per minute (5.0 gpm) leak. The new SQUIRT Version 2.4 statistical default values gave crack lengths 4 to 12 percent larger than the PICEP Version 4.0 default values. These differences were less for the 1.89 liters per minute (0.5 gpm) leak.

Tight Crack Leak-Rate Sensitivity Calculations. A second set of leak-rate sensitivity calculations (Set B) involved examining how the leak-rates change in the tight crack region with different leak-rate models. One of the difficulties with all leak-rate analyses comes from the friction factor equation used where the friction factor, f , is calculated by

$$f = [C_1 \ln(D_H/k) + C_2]^{-2} \quad (3-2)$$

where

C_1, C_2 = constants of 2.00 and 1.14, respectively (Ref. 3.14),

Figure 3.6a Comparison of calculated crack lengths for various default crack morphology parameters (IGSCC crack using the SQUIRT2 program)

Figure 3.6b Comparison of calculated crack lengths for various default crack morphology parameters (corrosion-fatigue crack using the SQUIRT2-program)

- D_H = hydraulic diameter which equals $4 \times \text{area} / \text{perimeter}$ of the leaking crack and which is a function of the crack length, the crack-opening displacement (COD), and the crack-opening shape (generally assumed to be elliptical), and
- k = surface roughness.

Hence, it can be seen that as the COD gets small (small D_H) relative to the surface roughness (k), then the friction factor (f) can go to zero, and is numerically undefined for smaller values of COD.

In Reference 3.9, it was found from examination of cracks removed from service that the roughness and number of turns may depend on the magnitude of the crack opening. If the crack is tight, the flow will follow all the turns, and the effective roughness might be that of a grain boundary (i.e., as in an IGSCC crack). This roughness was referred to as the local roughness, k_l . If the crack opening is large, then the flow is straight through the thickness and the roughness is the global value that might typically be measured with surface roughness devices over the entire flow path length. This roughness is referred to as the global roughness, k_g . For the tight COD case, the local roughness is small, but the number of turns and effective flow path are much greater than in the large COD case. These different flow conditions are illustrated in Figure 3.7. A transition model between these two flow conditions was proposed in Reference 3.13 and is illustrated in Figure 3.8.

An additional aspect found in Reference 3.13 from examining service-removed cracks, was that the flow path was found to be greater than the thickness of the pipe. For instance, a crack might follow a fusion line of a weld rather than grow straight through the thickness. The ratio of the flow path to pipe thickness was defined for both large crack openings (K_G) and tight crack openings (K_t). The tight crack opening K_t accounted for the pressure drop being larger for the longer flow path when there were local turns, i.e., the pressure drop for the number of turns in N_G or N_t does not include a pressure drop due to the longer flow path.

To assess the difference in calculated leak rates, a set of calculations was performed using the SQUIRT code with fixed roughness and number of turn values (using the average values from Reference 3.13), and using the COD-dependant crack morphology model. The values used are given in Table 3.2.

The calculations were made for a circumferential through-wall-crack opening geometry in which the outside surface had a crack length of 133 mm (5.25 inches) and an opening shape that was elliptical. The thickness of the pipe was 26.2 mm (1.03 inches). The pipe was filled with water at 288 C (550 F) and pressurized to 15.5 MPa (2,250 psi), which are PWR subcooled conditions. It was assumed that the crack-opening displacement was identical on the inside and outside surfaces of the pipe. The leak-rate was calculated for total center-crack-opening displacement values of 0.001 to 0.50 mm (0.000039 to 0.020 inches).

Figures 3.9a (for large COD values) and 3.9b (for small COD values) show the calculated leak rates versus COD. This shows that using the standard crack morphology model, i.e., roughness not dependant on COD, that the analysis becomes invalid at a COD of 0.094-mm (0.0037-inch) in Figure 3.9a. However, the COD-dependant crack morphology model gives results that are valid down to 0.025-mm (0.00098-inch) of COD as shown in Figure 3.9b. Also note that in Figure 3.9b, all the standard SQUIRT values are invalid because the friction factor equation limits are exceeded. In fact, it can be seen that for COD values less than 0.05 mm (0.0020 inches) that the term $[C_1 \ln(D_H/k) + C_2]$ is negative giving the strange shape to the leak-rate curve.

Figure 3.7 Local and global surface roughness and number of turns

Figure 3.8 Crack morphology variables versus normalized COD

Figure 3.9a Comparison of calculated leak rates between current SQUIRT Version 2.4 and NUREG/CR-6004 COD dependant crack morphology models (large leak rates)

Figure 3.9b Comparison of calculated leak rates between current SQUIRT Version 2.4 and NUREG/CR-6004 COD dependant crack morphology models (small leak rates)

Table 3.2 Summary of different crack morphology variables for Set B leak-rate analyses (for a corrosion-fatigue crack)

	Standard SQUIRT (V2.4) Model (NUREG/CR-6004 Average values)	NUREG/CR-6004 COD Dependent Model
Surface Roughness, μm (μin)		
Global (k_g)	40.5 (1,590)	40.5 (1,590)
Local (k_l)	N.A. ^(a)	8.81 (347)
Number of 90-degree turns, #/mm (#/in)		
Global (N_G)	0.673 (17.1)	0.673 (17.1)
Local (N_L)	N.A.	6.73 (171)
Flow Path/Thickness		
Global (K_G)	1.06	1.02
Local (K_L)	N.A.	1.6

(a) N.A. = not applicable.

3.4 Effects of Off-Centered Cracks During Leakage on the Maximum Load-Carrying Capacity for LBB Evaluations

The U.S. NRC's LBB analysis procedure is a very explicit procedure (Ref. 3.10) that is used for pipe systems that don't have large loads or mechanisms that could cause a crack to form in service. Nevertheless, the U.S. NRC LBB procedure assumes a hypothetical crack. Other LBB procedure may involve more detailed crack growth analyses, and additional considerations may be needed for a pipe system to behave in an LBB manner if there was some mechanism that could cause a crack to occur in service. One of these considerations involves the assumption that the leaking crack at normal operating conditions has a different bending plane than the bending plane at normal plus safe-shutdown earthquake (N+SSE) loading. The worst case would be for the leaking crack to be off-centered from the normal operating bending plane, and then to be centered for the N+SSE bending plane. The cracks could occur off the center of the bending plane due to random locations of workmanship flaws. (Note: additional probabilistic calculations on this effect are described in Section 4.0.)

The deterministic calculations involved used the off-center crack COD calculational procedure described in Reference 3.14. Here it is assumed that there is a 3.8 liters per minute (1 gpm) leaking crack, but the center of the crack would be at various angles to the bending plane. It was assumed that the crack could not extend below the neutral axis of the bending plane, that is, the crack wouldn't grow if the applied bending loads are compressive. (This is logical, except in the presence of high axial stresses and/or residual stresses.) These calculations also assumed:

- The SQUIRT Version 2.4 default leak-rate parameters for an IGSCC crack,

- Outside pipe diameters 356 mm (14 inches) with an R_m/t ratio of 10,
- A pressure of 15.51 MPa (2,250 psi) and a temperature of 288 C (550 F), and
- A bending stress to give a total pressure and bending stress of 50 percent of the Service Level A maximum allowable stress from ASME Section III Article NB-3650 for TP304 pipe.

The resulting crack lengths are shown in Figure 3.10 as a function of the angle of the off-centered crack. This shows that there was not a very large difference in the crack lengths until the center of the crack was more than 50 degrees off the center of the bending plane.

To see the effect on the entire LBB analysis procedure, fracture calculations were then made assuming the following conditions.

- It was assumed, for N+SSE loading that the crack is centered on the bending plane.
- The Net-Section-Collapse, LBB.NRC and GE/EPRI analysis methods were used to calculate the maximum loads.
- The average stress-strain curve properties for TP304 stainless steel base metal were used from the statistical analysis in Appendix B of Reference 3.13, i.e.,

$$\epsilon/\epsilon_o = \sigma/\sigma_o + \alpha(\sigma/\sigma_o)^n \quad (3-3)$$

where

$$\begin{aligned} \sigma_o/\epsilon_o &= E = 182.7 \text{ GPa (26,500 ksi)} \\ \sigma_o &= \sigma_y = 154.8 \text{ MPa (22.5 ksi)} \\ \alpha &= 8.073 \\ n &= 3.80 \end{aligned}$$

- The mean minus one standard deviation J-R curve for a stainless steel SAW weld from data in the PIFRAC data base (Ref. 3.16) was used. The mean minus one standard deviation J-R curve is

$$J = 73.4 + 83.5(\Delta a/r)^{0.643} \quad (3-4)$$

where

J is in units of kJ/m², Δa is in units of mm, and r has a value of 1 mm.

The change in the maximum load-carrying capacity was calculated relative to the centered leaking crack case, see Figure 3.11. These results show that the maximum load of the off-centered crack (under normal operating conditions) was less than 10 percent from the centered crack maximum load when the middle of

Figure 3.10 Calculated off-centered crack length for 3.785 l/min (1 gpm) leak rate versus angle from center of off-centered crack to the bending plane

Figure 3.11 Comparison of maximum loads for 3.785 l/min (1 gpm) leaking cracks that were off-centered during leakage but centered during N+SSE loading versus angle from center of off-centered crack to the bending plane

the off-centered crack was 50-degrees off the bending plane. This 10 percent difference is considered insignificant.

3.5 Effect of Restraint of Pressure Induced Bending in a Piping System on LBB

Another factor not generally considered in LBB analysis is that there is a restraint of pressure induced bending for a crack in a piping system. This effect was first observed in IPIRG-1 pipe system Experiment 1.3.7, where the calculated critical crack size, considering pressure loading alone, was 64 percent of the circumference, but the observed crack length at instability was 95 percent of the pipe circumference. This difference in crack sizes was explained by the effect of restraint of pressure induced bending. That is, the critical crack length solutions (EPFM and Net-Section-Collapse analyses) for a circumferential crack in a pipe under axial tension assume that the ends of the pipe are free to rotate. With the free rotation, there is an induced bending from axial forces times an eccentricity equal to the distance from the center of the crack plane to the center of the pipe. In a pipe system, this induced bending can be restrained and then the load-carrying capacity increases. This difference for the Net-Section-Collapse analysis is illustrated in Figure 3.12. Hence, there is some additional load-carrying capacity for a through-wall crack in a pipe due to this effect.

One factor briefly investigated in Reference 3.15, was that the restraint of induced bending also affects the crack-opening displacement even under elastic loading. In this case, the COD is less than that calculated by typical LBB analyses that assume the pipe is free to rotate under pressure loading. The reduction of the COD was calculated by elastic analysis in Reference 3.15, and was found to be affected by the length of the through-wall crack, and the location of the crack from a restrained location, see Figure 3.13. This reduction in the COD is detrimental to LBB acceptance, but is not very significant for short crack lengths.

Because of the trade-offs of the effects on restraint of pressure induced bending on COD versus maximum loads, sample deterministic LBB calculations were made. Probabilistic calculations are also discussed later in this report. The deterministic calculations made involved 114.3 and 711.2 mm (4.5 and 28 inch) outside pipe diameters. The pipe diameter was varied because the LBB crack size is a larger percent of the circumference for a smaller diameter pipe, and hence the restraint of bending is more significant for the small diameter pipe as can be inferred from Figure 3.13.

Calculations of the leakage crack size were made with and without the restraint of the pressure using the COD restraint values in Figure 3.13. Using the normalized restraint length of 1.0 represented the case of a crack close to a nozzle or elbow. The following conditions were used in this analysis:

- The SQUIRT Code Version 2.4 was used with the IGSCC crack default parameters.
- The R_m/t ratio was 6 for the 114.3 mm (4.5 inch) and 10 for the 711.2 mm (28 inch) pipe.
- The pressure was 15.51 MPa (2,250 psi) and the temperature was 288 C (550 F).

Figure 3.12 Net-Section-Collapse analyses predictions, with and without considering induced bending, as a function of the ratio of through-wall-crack length to the pipe circumference and comparison to IPIRG-1 Experiment 1.3-7 crack length at DEGB

Figure 3.13 Effects of fully restrained bending conditions from crack location on COD normalized by the unrestrained COD

- A bending stress chosen to give a total pressure and bending stress of 50 percent of the Service Level A maximum allowable stress from ASME Section III Article NB-3650 for TP304 pipe was used.

Initial calculations were made with a leak rate of 37.8 liters per minute (10 gpm). This was to correspond to a 3.78 l/min (1.0 gpm) leak times a safety factor of 10. Analyses were successfully conducted for the large diameter pipe, but solutions could not be obtained for the small diameter pipe. The leak rate was subsequently reduced to 1.89 l/min (0.5 gpm) and a solution for both the large and small diameter pipes were obtained. Table 3.3 shows the difference in the through-wall-crack lengths.

Table 3.3 Differences in leakage flaw sizes due to restraint of pressure induced bending

Outside Pipe Diameter		Leakage Crack Length, θ/π	
mm	inches	Restrained	Unrestrained
114.3	4.5	0.7250	0.2360
711.2	28.0	0.0219	0.0219

The next step in the deterministic LBB analysis was to assess the effect of restraint of pressure induced bending on the maximum load-carrying capacity. To do this, the LBB.ENG2 analysis procedure, Ref. 3.17, was modified to eliminate the induced bending from the axial tension stress component. (A special modification of NRCPIPE Version 3.0 was used in these calculations.) Hence, maximum load calculations were made in the unrestrained and restrained conditions for the corresponding leakage cracks. These calculations assumed the following:

- The crack is centered on the bending plane,
- The average stress-strain curve properties for TP304 stainless steel base metal were used from the statistical analysis in Appendix B of Reference 3.13, as were previously given in Equation 3-3, and
- The crack was assumed to be in the weld, hence the mean minus one standard deviation J-R curve for a stainless steel SAW weld was used, see Equation 3-4.

The results of the maximum load calculations are shown in Table 3.4.

Table 3.4 Differences in maximum stresses^(a) for LBB analyses due to restraint of pressure induced bending

Outside Pipe Diameter		Restrained/Unrestrained Maximum Stresses
mm	inches	
114.3	4.5	0.1129
711.2	28.0	1.007

(a) Maximum stress = nominal bending plus tension stresses.

These results showed that the effects of the restraint of pressure induced bending on the maximum load was insignificant for the large diameter pipe. However, for the small diameter pipe, the effect was significant. This comes about because: (1) the difference in the leakage crack length was negligible for large diameter pipe with a short crack, and (2) for the small diameter pipe the effect on the leakage crack length is very large and the higher load-carrying capacity for the restraint of pressure induced bending loads offers little compensation for the large crack size. This result is significant in regard to application of LBB to smaller diameter pipe. The trend for other intermediate pipe sizes is not obvious.

3.6 Effect of Weld Residual Stresses on LBB Analysis

Another factor that has not been given much attention in LBB analysis is the effect of weld residual stresses. Residual stresses could potentially affect the maximum load-carrying capacity as well as the crack-opening displacement. For nuclear piping, carbon steel welds less than 38.1-mm (1.5-inches) thick are typically not stress relieved. Austenitic pipe occasionally have solution-annealed welds, if the whole pipe subassembly can be heat-treated, but there are tie-in welds that would not be solution annealed.

Work in the Degraded Piping Program - Phase II on as-welded and solution-annealed stainless steel SAWs with circumferential cracks, showed that there were no detrimental effects from the residual stresses on the fracture behavior (crack initiation and maximum loads) of the as-welded pipes, Ref. 3.18. Interestingly, the solution-annealed pipes actually had lower maximum loads than the as-welded pipes with identical cracks. This is believed to be due to the lowering of the yield strength of the weld metal from the solution annealing. (See Reference 3.19 for additional analyses accounting for weld metal strength properties in fracture analyses.)

In References 3.15 and 3.20, the effects of residual stress on the crack-opening displacement were investigated. These analyses showed that with thinner pipe (less than 25-mm [1-inch] thick), the tension-compression residual stress fields through the thickness caused a significant rotation of the crack faces. For thicker welds, the residual stress field may be tension-compression-tension through the thickness, and the resulting rotation of the crack faces is much smaller. The magnitude of the crack-face rotation relative to the crack-opening displacement from the applied loads would determine the effect on leak rates. The crack-face rotation from the residual stresses would make the crack-opening displacement on the inner surface larger, and the crack-opening displacement on the outside surface smaller. The average crack-opening displacement would be the same, unless the crack faces rotated enough to touch.

One question is what would be the effect of the crack-face rotation on leak rates. Unpublished data from Nuclear Electric* showed that converging and diverging crack faces gave the same leakage. This information suggests that the crack-face rotation is not important on the leak rate unless the crack faces touch. This observation has been substantiated with calculations using the SQUIRT leak-rate computer code.

Typically, LBB analyses calculate the center-crack-opening displacement and use the assumption of an elliptical crack-opening profile. Finite element analyses by many investigators have shown that the elliptical profile is a good approximation (Ref. 3.15). Hence, it is quite possible that the residual stresses

* Results from Mr. T. Chivers to Dr. G. Wilkowski.

may pinch off the flow at the ends of the crack, and effectively make the crack length shorter. In fact, this was illustrated by thermal-plastic and linear-elastic finite-element superposition methods in Reference 3.20, see Figure 3.14. The results by Brickstad and Moberg in Figure 3.14 involved linear elastic superposition of residual stress fields on a crack face as part of an IPIRG-2 round-robin analysis effort (Ref. 3.12). The Battelle solution in this same figure used thermal-plastic finite-element analyses to simulate the weld residual stress field, then unpinned the crack-face nodes to determine the crack-opening displacement. Both analyses show the crack face pinching off the flow on the outside surface of the pipe.

Additionally in Reference 3.15, some sensitivity studies were conducted to assess the effects of operating stresses on the crack-opening displacement. This analysis involved a superposition of the crack-face closure and the crack opening due to the applied loads, which is valid for linear elastic analyses. The results are shown in Figure 3.15a and 3.15b for thick and thin welds, respectively. The calculated crack-opening displacement at the center of the crack was determined with an applied moment and the residual stresses (δ_{M+RS}), and with only the applied moment (δ_M), i.e., without residual stresses. When the normalized difference, $100[\delta_{M+RS} - \delta_M]/\delta_M$, is -100 then the crack faces at the center of the crack length are in contact and the flow is pinched off. This pinched-off flow condition occurs for the thin-wall pipe at 28.6-percent of the Service Level A stresses. For the thicker pipe, the pinching of the flow would not occur unless the applied stresses were less than 10 percent of the Service Level A stresses.

Figure 3.14 Total crack-opening displacement due to residual stresses and pressure

Figure 3.15a Percentage change in calculated COD due to residual stress as a function of applied moment for the thick-walled large-diameter pipe

Figure 3.15b Percentage change in calculated COD due to residual stress as a function of applied moment for the thick-walled small-diameter pipe

To further assess the effect of the crack-face rotations at different stress levels, calculations were made to assess how the effective crack length for leak-rate analysis might change at higher than the entire pinched-flow condition. Here, the crack-face rotations from the residual stresses were superimposed on the elliptical crack-opening shape to determine the length of crack that was pinched. Recall that the crack-rotation by itself has little effect on the leak rate, hence the change in leak-rate could be estimated as the change in the crack length from the pinched-off point to the center of the crack. (The effect of a longer crack length on the inside surface versus the outside surface on the fluid flow was neglected, making these leakage predictions lower bounding.)

Figure 3.16 shows the estimated change in leak rate as a function of the ASME Service Level A stresses. The trend with the thick-pipe weld residual stress field is much less than that for the thin-pipe weld residual stress field. For the thick-pipe case, the effect of the residual stresses on leak-rate is insignificant for normal operating stresses greater than 10 percent of the maximum ASME Service Level A stress limit.

For the thin-pipe case, the leak rate is zero at 28.6-percent of the Service Level A stress, and at 50-percent of the maximum allowable Service Level A stresses the leak-rate with residual stresses would be about 80 percent of that without residual stresses. Hence, the effect of residual stresses is more significant for the thin-pipe case, particularly at low applied stress levels.

As an additional effort, the effect of high-pressure hydrostatic testing was evaluated using the Battelle thermal-plastic finite analysis in Reference 3.20. Figure 3.17 shows the effect of the high-pressure proof test is to locally yield the weld, so that the effects of the residual stresses on the crack opening are less detrimental for LBB analyses.

Figure 3.16 Ratio of leak rates, with and without residual stresses, as a function of applied loads

Figure 3.17 Comparison of CODs predicted with and without proof-pressure loading effect

The results of these analyses show that residual stresses can significantly affect the leak rate for pipes welded with tension-compression residual stress fields through the thickness. This is more likely to occur with thinner pipe and hence smaller diameter pipe. An effective countermeasure to this would be to stress-relieve the welds or hydro test to a sufficiently high enough level to reduce the residual stress fields. Either of these countermeasures would help to mitigate subcritical crack growth as well.

3.7 References

- 3.1 Wilkowski, G. M., Olson, R., Scott, P., Brust, F., Ghadiali, N., Kilinski, T., Rudland, D., Hopper, A., "The IPIRG Programs - Advances in Pipe Fracture Technology," *Proceedings of LBB'95 Conference*, Lyon France, October 1995.
- 3.2 Krishnaswamy, P., Scott, P., Mohan, R., Rahman, S., Choi, Y. H., Brust F., Kilinski, T., Ghadiali, N., Marschall, C., and Wilkowski, G., "Fracture Behavior of Circumferential Short-Surface-Cracked Pipe," NUREG/CR-6298, November 1995.
- 3.3 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.
- 3.4 Scott, P. M., Wilson, M., Olson, R., Marschall, C., Schmidt, R., and Wilkowski, G., "Stability of Cracked Pipe Under Inertial Stresses - Subtask 1.1 Final Report," NUREG/CR-6233, Vol. 1, August 1994.

- 3.5 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.
- 3.6 Olson, R., Scott, P., Marschall, C., and Wilkowski, G., "Comparison of Fracture Toughness Values from an IPIRG-1 Large-Scale Pipe System Test and C(T) Specimens on Wrought TP304 Stainless Steel," ASME PVP Vol. 280, pp 241-254, June 1994.
- 3.7 "Evaluation of Flaws in Ferritic Piping," EPRI Report NP-6045, prepared by Novetech Corporation, October 1988.
- 3.8 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.
- 3.9 Kumar, V., and German, M., "Elastic-Plastic Fracture Analysis of Through-Wall and Surface Flaws in Cylinders," EPRI report NP-5596, January 1988
- 3.10 Solicitations for public comment on "Standard Review Plan 3.6.3 LEAK-BEFORE-BREAK PROCEDURES," *Federal Register*, Vol. 52, No. 167, Friday, August 28, 1987, Notices, pp 32626 to 32633.
- 3.11 Scott, P., Kramer, G., Vieth, 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, pp 207-220, June 1994.
- 3.12 Rahman, S., Olson, R., Rosenfield, A., and Wilkowski, G., "Summary of Results from the IPIRG-2 Round-Robin Analyses," NUREG/CR-6337, January 1996.
- 3.13 Rahman, S., Ghadiali, N., Paul, D., and Wilkowski, G., "Probabilistic Pipe Fracture Evaluations for Leak-Rate Detection Applications," NUREG/CR-6004, April 1995.
- 3.14 Paul, D. D., Ahmad, J., Scott, P. M., Flanigan, L. F., and Wilkowski, G. M., "Evaluation and Refinement of Leak-Rate Estimation Models," NUREG/CR-5128 Rev. 1, June 1994.
- 3.15 Rahman, S., Brust, F., Ghadiali, N., Choi, Y. H., Krishnaswamy, P., Moberg, F., Brickstad, B., and Wilkowski, G., "Refinement and Evaluation of Crack-Opening Analyses for Short Circumferential Through-Wall Cracks in Pipes," NUREG/CR-6300, April 1995.
- 3.16 Wilkowski, G. M. and others, "Short Cracks in Piping and Piping Welds," Seventh Semiannual Report, NUREG/CR-4599, Vol. 4, No. 1, April 1995.
- 3.17 Brust, F., Scott, P., Rahman, S., Ghadiali, N., Kilinski, T., Francini, R., Marschall, C., Muira, N., Krishnaswamy, P., and Wilkowski, G., "Assessment of Short Through-Wall Circumferential Cracks in Pipes - Experiments and Analyses," NUREG/CR-6235, April 1995.

- 3.18 Wilkowski, G. M., and others, "Analysis of Experiments on Stainless Steel Flux Welds," NUREG/CR-4878, April 1987.
- 3.19 Rahman, S., Brust, F., and Wilkowski, G., "Analyses of Full-Scale Pipe Fracture Experiments on Stainless Steel Flux Welds," ASME PVP Vol. 280, pp 241-254, June 1994.
- 3.20 Rahman, S., Dong, P., Wilkowski, G. M., Brickstad, B., Moberg, F., "Effect of Weld Residual Stresses on the Crack-Opening-Area Analysis of Pipes for LBB Applications," *Proceedings of LBB'95 Conference*, Lyon France, October 1995.

4.0 PROBABILISTIC LBB ANALYSIS

4.1 Introduction

The application of the LBB methodology requires: (1) knowledge of the pipe loads during various operating conditions of power plants, (2) details of geometry and material properties of the pipe, (3) knowledge of the anticipated cracking mechanisms and the resulting crack morphology variables for leak-rate analyses, and (4) methods for thermal-hydraulic and fracture mechanics analyses of a flawed pipe. Some of these items are subject to inherent statistical variability. Therefore, a treatment of these uncertainties and an assessment of their impact on system performance can be based on theories of probability and structural reliability.

4.2 Probabilistic LBB Methodology

During the NRC's Short Cracks in Piping and Piping Welds Program, a probabilistic LBB methodology was developed (Ref. 4.1) based on the general guidelines proposed in NUREG-1061, Volume 3, Ref. 4.2. The steps of the probabilistic evaluation are very similar to the steps of the deterministic LBB methodology. There are two important steps to an LBB analysis. These factors are the determination of the leaking size flaw for a given leak rate, and then determining if that flaw can tolerate the maximum loads at normal plus safe-shutdown earthquake loading (N+SSE). Details of the probabilistic methodology used are summarized in Appendix A. Important conditions in the conditional failure probability values determined are; (1) the seismic event occurs with a probability of 1.0, and (2) the leakage occurs with a probability of 1.0. The conditional failure probabilities in this report could be multiplied by the seismic event probability and probability of the leaking crack to get an absolute probability of failure.

4.3 Piping Systems for Probabilistic LBB Evaluations

The probabilistic model developed in Reference 4.1 was applied to various nuclear piping systems in BWR and PWR plants for calculating the conditional probability of failure. Pipe sizes were selected with large, intermediate, and small diameters typically used. Two pipes of each size were considered with austenitic and ferritic materials. The BWR piping systems included side riser, main steam, recirculation branch, feedwater, bypass, and reactor water clean-up lines. The PWR piping systems included main coolant, surge, feed-water, spray, and steam generator blowdown lines. Several cracking mechanisms, such as intergranular stress corrosion cracking, corrosion fatigue, and thermal fatigue, were also considered. The crack location was defined in both a deterministic sense (either base metal or weld metal) and a probabilistic sense (random location). Table 4.1 shows the through-wall-cracked BWR and PWR piping systems which were originally considered in Reference 4.1. Some of these pipes were used for probabilistic pipe fracture evaluations in the present study.

Table 4.1 BWR and PWR piping systems for probabilistic fracture evaluations

Cases	Piping System	Nominal Diameter, mm (inches)	Thickness, mm (inches)	Base Metal	Weld ^(a) Metal	Assumed ^(b) Cracking Mechanism
(a) Through-wall-cracked BWR Piping Systems						
BWR-1	Side Riser	711.2 (28)	35.8 (1.41)	TP304	SS Flux	IGSCC
BWR-2	Main Steam	711.2 (28)	35.8 (1.41)	A516 Gr70	CS Flux	Corrosion Fatigue
BWR-3	Recirculation Branch Line	457.2 (18)	23.9 (0.94)	TP304	SS Flux	IGSCC
BWR-4	Feedwater	457.2 (18)	39.4 (1.55)	A106B	CS Flux	Corrosion Fatigue
BWR-5	Bypass Line	101.6 (4)	8.51 (0.34)	TP304	SS Flux	IGSCC
BWR-6	Reactor Water Clean-up	101.6 (4)	8.51 (0.34)	A106B	CS Flux	Corrosion Fatigue
(b) Through-wall-cracked PWR Piping Systems						
PWR-1	Main Coolant	812.8 (32)	76.2 (3.00)	CF8M	SS Flux	Thermal Fatigue
PWR-2	Main Coolant	812.8 (32)	76.2 (3.00)	A516 Gr70	CS Flux	Corrosion Fatigue
PWR-3	Surge Line	355.6 (14)	35.8 (1.41)	CF8M	SS Flux	Thermal Fatigue
PWR-4	Feedwater	355.6 (14)	35.8 (1.41)	A106B	CS Flux	Corrosion Fatigue
PWR-5	Spray Line	101.6 (4)	13.5 (0.53)	TP304	SS Flux	IGSCC
PWR-6	Steam Generator Blowdown Line	101.6 (4)	13.5 (0.53)	A106B	CS Flux	Corrosion Fatigue

(a) SS = stainless steel; CS = carbon steel; Flux = submerged arc weld (SAW) or shielded metal arc weld (SMAW).

(b) IGSCC = intergranular stress-corrosion cracking

4.4 Probabilistic Sensitivity Analyses

A two-step procedure developed in Reference 4.1 was used for conducting the sensitivity analyses. First, for a given pipe with known material properties, loads, leak rate, and crack-morphology parameters, the mean and standard deviation of a leakage size flaw were calculated. In Reference 4.1, it was found that the flaw size with these distribution parameters would follow lognormal probability fairly well. Second, with the calculated flaw size and known material properties, the conditional probability of failure was calculated. The computer codes, PSQUIRT and PROLBB and their respective derivatives, developed for various required modifications, were used to calculate the flaw size and conditional failure probability, respectively.

The results of sensitivity analyses for each of the parameters considered in this study are given in the next few subsections. In determining the conditional probability of failure, individual sets of calculations were developed for a crack located either in the base metal or in the weld metal. A weighted average of these

probabilities, based on the actual likelihood of cracks found in service, was developed to determine the conditional probability of failure for a randomly located crack (Ref. 4.1).

4.4.1 Normal Operating Stresses

For piping systems to be evaluated, the normal operating stresses (σ_N) are needed to determine the crack size for a given leak rate. However, the actual normal operating stresses and their probabilities of occurring for all plants in the U.S. are difficult to quantify. To simplify this effort, it was assumed that the limits of ASME Section III Code stress level can be applied, even though actual stresses might be lower. Hence, several stress values equal to 25, 50, and 100 percent of the ASME Service Level A stress limit (σ_{SL-A}) were used. For Class 1 piping, the Service Level A limit is $1.5S_m$, where S_m is the material design stress intensity from ASME Section III, Appendix I (Ref. 4.3). The stresses were assumed to be deterministic.

Table 4.2 shows the calculated mean values of the leakage size flaws for pipes BWR-1, BWR-2, PWR-1, and PWR-2 using the PSQUIRT code. They were developed for various combinations of normal operating stresses and detectable leak rates. The results from Table 4.2 suggests that for a given leak rate, the crack size increases very rapidly with the reduction of normal operating stresses. This can have a significant effect on the failure probability of pipes and is discussed in the next section.

4.4.2 Normal Plus Safe-Shutdown Earthquake Stresses

One of the most difficult aspects of this analysis was the selection of normal plus safe-shutdown earthquake stresses (σ_{N+SSE}) for application to a large numbers of piping systems and plant locations in the U.S. It was beyond the scope of this effort to analyze all plants and piping systems. Instead, the ASME Section III stress limits from Service Levels B, C, and D were used in this study. According to the 1994 and earlier ASME Section III of the ASME code, the Service Levels B, C, and D are defined as the lower values of $1.8S_m$ and $1.5S_y$, $2.25S_m$ and $1.8S_y$, and $3S_m$ and $2S_y$, respectively. The stresses under N+SSE conditions were also assumed to be deterministic.

Figures 4.1 through 4.12 show the plots of the conditional failure probability (CPOF) of pipes BWR-1, BWR-2, PWR-1, and PWR-2 as a function of leak rate for various combinations of normal operating and N+SSE stresses. The above probabilities were calculated separately when the crack was assumed to be located (1) in base metal, (2) in weld metal, or (3) randomly located, where the probability considered was 2/3 for cracks in base metal and 1/3 for cracks in either weld metal or the fusion line (see Section 5.0 in Reference 4.1).

The results indicate that the failure probability increases when either the normal operating stress decreases or the N+SSE stress increases. Somewhat surprisingly, the conditional probability of failure is much higher due to the reduction of normal operating stresses than when the N+SSE stresses were increased to the Service Level D limit. At first, it was thought that increasing the N+SSE stresses to the Service Level D limit would cause the conditional failure probabilities to go to 1.0. This is because the Service Level D stresses limit is the lower of $3S_m$ or $2S_y$, which should be close to the flow stress of the material for uncracked pipe. Hence, the pipe with a leaking crack might not tolerate these loads with any size flaw. This would be true if the calculations used the Code properties and elastic stress analyses. However, there were two factors that changed this result considerably. First, the probabilistic analyses used the statistical

Table 4.2 Mean crack length for various leak rates and normal operating stresses for a symmetrically centered crack with unrestrained pressure-induced bending

Leak Rate l/min (gpm)	Mean Crack Length, m (in)			
	BWR-1	BWR-2	PWR-1	PWR-2
(a) $\sigma_N/\sigma_{SL-A} = 0.25$				
0.379 (0.1)	0.223 (8.79)	0.136 (5.36)	0.231 (9.08)	0.134 (5.27)
3.79 (1)	0.442 (17.3)	0.265 (10.4)	0.408 (16.1)	0.258 (10.2)
37.9 (10)	0.684 (26.9)	0.469 (18.4)	0.648 (25.5)	0.475 (18.7)
379 (100)	0.765 (30.1)	0.736 (24.0)	0.885 (34.8)	0.714 (28.1)
(b) $\sigma_N/\sigma_{SL-A} = 0.50$				
0.379 (0.1)	0.074 (2.91)	0.056 (2.21)	0.052 (2.04)	0.045 (1.79)
3.79 (1)	0.179 (7.05)	0.131 (5.14)	0.116 (4.56)	0.102 (4.02)
37.9 (10)	0.321 (12.6)	0.272 (10.7)	0.252 (9.92)	0.230 (9.07)
379 (100)	0.452 (17.8)	0.533 (21.0)	0.473 (18.6)	0.455 (17.9)
(c) $\sigma_N/\sigma_{SL-A} = 1.0$				
0.379 (0.1)	0.020 (0.77)	0.028 (1.09)	0.019 (0.74)	0.022 (0.86)
3.79 (1)	0.046 (1.81)	0.065 (2.55)	0.044 (1.72)	0.052 (2.03)
37.9 (10)	0.101 (3.98)	0.146 (5.76)	0.099 (3.89)	0.118 (4.63)
379 (100)	0.130 (5.12)	0.309 (12.2)	0.223 (8.76)	0.264 (10.4)

strength properties of the material investigated, and on the average the actual flow stress was about 35 percent higher than if the Code values were used. Secondly, the probabilistic analysis recognized that elastic stress analysis values above yield will give higher moments than would actually occur. Figure 5.2 in Reference 4.1 shows a simple correction applied to the elastically calculated stresses. This correction involves keeping the elastic stresses the same if they were equal to or below yield, however, if the elastic stresses reached the flow stress, $(\sigma_y + \sigma_u)/2$, then there was a correction of $\pi/4$ on the elastic stress to account for fully plastic behavior in calculating moments. Between yield and flow stress, a linear interpolation was used. This second correction had the effect of reducing the Service Level D elastic stresses, so that the difference between the applied stresses and the material's flow stress becomes even greater. Hence, any uncertainty in the normal operating stresses will have relatively larger effects on the failure probability than the assumption of the N+SSE stresses.

One major implication from this work is how to conduct an LBB analysis. For instance, NRC SRP 3.6.3 for LBB analyses states to "Identify the location(s) at which the highest stresses coincident with the poorest material properties occur...". As can be seen in Figures 4.1 to 4.12, the conditional failure probabilities are the highest when the normal operating stresses are low. These figures show that a location with lower N+SSE stresses and low normal operating stresses could have a higher failure probability than a location with higher normal operating stresses and higher N+SSE stresses. Hence, the LBB criteria needs to be more specific on what stresses are the highest, i.e., either the normal operating stresses or normal plus SSE stresses. Since a crack is more likely to occur with high normal operating stresses, one logical choice would be to select the locations with the highest normal operating stresses for LBB analyses.

Figure 4.1 Conditional failure probability of BWR-1 for various combinations of normal operating and N+SSE stresses (base metal)

Figure 4.2 Conditional failure probability of BWR-1 for various combinations of normal operating and N+SSE stresses (weld metal)

Figure 4.3 Conditional failure probability of BWR-1 for various combinations of normal operating and N+SSE stresses (random crack location)

Figure 4.4 Conditional failure probability of BWR-2 for various combinations of normal operating and N+SSE stresses (base metal)

Figure 4.5 Conditional failure probability of BWR-2 for various combinations of normal operating and N+SSE stresses (weld metal)

Figure 4.6 Conditional failure probability of BWR-2 for various combinations of normal operating and N+SSE stresses (random crack location)

Figure 4.11 Conditional failure probability of PWR-2 for various combinations of normal operating and N+SSE stresses (weld metal)

Figure 4.12 Conditional failure probability of PWR-2 for various combinations of normal operating and N+SSE stresses (random crack location)

4.4.3 Off-Center Cracks

In conducting both leak-rate and flow stability analyses, a postulated through-wall-crack size (leakage flaw) is often calculated based on its symmetric placement with respect to the bending plane of the pipe. This is usually justified with the reasoning that the tensile stress due to bending is largest at the center of this symmetric crack. However, fabrication imperfections will occur randomly around the pipe circumference. Additionally, during the normal operating condition of a plant, the stress component due to pressure is more significant than that due to bending. As such, the postulated leakage flaw may be off-centered and can thus be located anywhere around the pipe circumference.

In the past, an analytical study to investigate the crack-opening characteristics of a pipe with an off-centered crack was performed (Ref. 4.4). Methods were developed by both finite element and estimation analyses to predict the crack-opening area for an off-centered crack. The results from that study showed that the crack-opening area for a pipe with an off-centered crack can be determined by normal analysis procedures for a symmetrically centered crack by resolving the applied moment to the effective moment at the center of the off-centered crack and assuming an elliptical profile for the crack-opening shape. This was an important finding since for leak-rate calculations, accuracy in the prediction of crack-opening area is more significant than that of the entire crack-opening shape.

In this study, some pipe specific calculations were performed to determine the effects of off-center cracks on the leakage size flaw under normal operating conditions. Due to uncertainty, the angle of off-center, measured from a vertical axis of symmetry (of a pipe cross-section), was assumed to be a random variable with equal probability (uniform distribution) of occurrence in the range $[0, \pi/2]$. Because of the off-center cracks, the crack-opening-area modules of the PSQUIRT code were appropriately modified according to the results obtained in Reference 4.4. To evaluate their effects on pipe fracture probability, it was, however, assumed that the crack was symmetrically centered during N+SSE condition. This was mainly due to the lack of current methods available to perform fracture-mechanics evaluations (e.g., calculating J-integral and the resulting load-carrying capacity) of flawed pipes with off-center cracks. Hence, the results of failure probability calculations, which involved leak-rate analysis of off-center cracks, but flow-stability analysis of symmetric cracks, should be interpreted carefully.

Table 4.3 shows the calculated mean values of leakage size flaws for BWR-1 and BWR-2 pipes for various leak rates and normal operating stresses when the cracks are off-centered. The comparisons of these mean flaw sizes with those calculated for symmetric cracks from Table 4.2 indicate that an off-centered crack can become much longer than a symmetrically centered crack for a given leak-rate detection. Correspondingly, the results of conditional failure probability calculations, shown in Figures 4.13 through 4.18 for BWR-1 and BWR-2 pipes, suggest that an off-centered crack can lead to a higher probability of failure for a given leak rate. This was demonstrated for both the austenitic (BWR-1) and ferritic (BWR-2) pipes. Note that the change in the conditional failure probability due to the off-centered crack increased more for higher normal operating stress levels than for low normal operating stress level flaw locations.

4.4.4 Restraint of Pressure-Induced Bending

Current structural analyses for TWC pipes subjected to axial tension loads (generally pressure induced) or combined bending and tension loads assume that the pipe is free to rotate during the application of the pressure loads. The restraint of the rotation associated with the axial membrane stresses increases the

Table 4.3 Mean Crack Length for various leak rates and normal operating stresses for off-center cracks or restrained pressure-induced bending

Leak Rate l/min (gpm)	Mean Crack Length, m		
	Off-Center Cracks		Restrained Pressure-Induced Bending
	BWR-1	BWR-2	BWR-1
	(a) $\sigma_N/\sigma_{SL-A} = 0.25$		
0.379 (0.1)	0.257 (10.1)	0.153 (6.00)	0.225 (8.85)
3.79 (1)	0.487 (19.2)	0.285 (11.2)	0.458 (18.0)
37.9 (10)	0.706 (27.8)	0.488 (19.2)	0.726 (28.6)
379 (100)	0.792 (31.2)	0.750 (19.5)	0.827 (32.6)
(b) $\sigma_N/\sigma_{SL-A} = 0.50$			
0.379 (0.1)	0.091 (3.59)	0.082 (3.24)	0.075 (2.93)
3.79 (1)	0.209 (8.22)	0.173 (6.81)	0.180 (7.08)
37.9 (10)	0.353 (13.9)	0.328 (12.9)	0.324 (12.8)
379 (100)	0.457 (18.0)	0.577 (22.7)	0.471 (18.5)
(c) $\sigma_N/\sigma_{SL-A} = 1.0$			
0.379 (0.1)	0.027 (1.04)	0.043 (1.70)	0.020 (0.77)
3.79 (1)	0.062 (2.44)	0.097 (3.81)	0.046 (1.81)
37.9 (10)	0.129 (5.07)	0.202 (7.97)	0.104 (4.08)
379 (100)	0.186 (7.31)	0.435 (17.1)	0.130 (5.13)

Figure 4.13 Conditional failure probability of BWR-1 for various crack orientations considering off-centered cracks (base metal)

Figure 4.14 Conditional failure probability of BWR-1 for various crack orientations considering off-centered cracks (weld metal)

Figure 4.15 Conditional failure probability of BWR-1 for various crack orientations considering off-centered cracks (random crack location)

Figure 4.16 Conditional failure probability of BWR-2 for various crack orientations considering off-centered cracks (base metal)

Figure 4.17 Conditional failure probability of BWR-2 for various crack orientations considering off-centered cracks (weld metal)

Figure 4.18 Conditional failure probability of BWR-2 for various crack orientations considering off-centered cracks (random crack location)

failure stresses (Refs. 4.1, 4.5, 4.6, and 4.7), but can decrease the crack-opening at a given load. If the pipe system restrains the bending, for example from cracks being close to a nozzle or restraint from the rest of the piping system, then the leak rate will be less than the leak rate calculated by using analyses that assume that the pipe is free to rotate. This will cause the leakage crack size to be larger than that calculated by the current analysis methods for the same leak rate. However, for a pipe which restrains bending from pressure loads, its load-carrying capacity will also increase. It is interesting to see how this would counter the effects of crack-opening reduction. Some scoping calculations were performed to assess the magnitude of the trends.

In References 4.4, a numerical procedure, based on a linear-elastic finite element method, was developed to quantify the effects of restraint of induced bending due to pressure on the crack-opening displacement (COD) of pipes for large diameter pipes (Case BWR-1, see Table 4.1). Figure 4.19 shows the plot of a correction factor for the selected piping system, defined as the ratio of restrained center COD to unrestrained center COD, as a function of normalized restraint length, defined as the ratio of restraint length (i.e., distance of the cracked plane from the restrained plane, L_R) to the mean pipe diameter (D_m), for pipes with $R_m/t = 10$ and $R_m/t = 6$ with three different crack sizes, $\theta/\pi = 1/8$, $1/4$, and $1/2$. Note: in the scoping FEM analysis, the restraint was modeled symmetrically, which may be conservative compared with nonsymmetrical crack locations for pragmatic cases. Using Figure 4.19, one should be able to determine the appropriate reduction of crack-opening area knowing the normalized restraint length and crack size. For an arbitrary crack size, the correction factor was determined by a linear interpolation of the curves shown in Figure 4.19. To determine the effects of pressure-induced bending, it was assumed that the crack was located very close to the restrained section. Hence, the normalized restraint length was

Figure 4.19 Effects of fully restrained bending conditions from crack location on COD normalized by the unrestrained COD

chosen to be one. As before, the crack-opening-area modules of PSQUIRT were modified by applying the correction factor from Figure 4.19 to the crack-opening component due to the tension load (pressure-induced). For calculating the maximum load, the PROLBB code was modified to drop the pressure-induced bending component in evaluating the J-integral. See Reference 4.1 for explicit details of the J-integral equations.

Table 4.3 also shows the calculated mean values of leakage size flaws for the BWR-1 pipe for various leak rates and normal operating stresses when the induced bending due to the pressure load is restrained. In general, the length of flaw sizes increased due to the restraint of induced bending when compared with the results of Table 4.2. But, no large differences were found between the flaw sizes obtained from the unrestrained and restrained conditions. This was mainly because the calculated COD due to the pressure was relatively small compared with that due to bending. Hence, the reduction of total COD due to pressure-induced bending from Figure 4.19 was not significantly large. The subsequent results of the probabilistic analysis, presented in Figures 4.20 and 4.21, however, showed that there was a marginal decrease of the conditional failure probability due to the increase of failure loads at restrained condition. Hence, for this particular large diameter pipe (Case BWR-1 in Table 4.1), the restraint of pressure-induced bending did not seriously affect the conditional probability of failure. This is consistent with the prior deterministic calculations, but probabilistic analyses for small diameter pipe were not conducted.

Figure 4.20 Conditional failure probability of BWR-1 for pressure-induced restraint condition (base metal)

Figure 4.21 Conditional failure probability of BWR-1 for pressure-induced restraint condition (weld metal)

4.4.5 Dynamic and Cyclic Loading Rates

For an LBB analysis, it is usually required to determine the fracture stability of a leakage size flaw under N+SSE stresses. This requires characterization of material properties from laboratory tests that can be used to calculate the load-carrying capacity of a cracked pipe. Typically, the strength (stress-strain curve) and fracture toughness (J-R curve) properties that are used for LBB analysis are obtained under quasi-static and monotonic loading condition. However, due to the dynamic and cyclic nature of the seismic ground motion, questions arise about the adequacy of quasi-static properties and their use for the prediction of failure probability as compared with that using more realistic dynamic and cyclic material properties of a pipe. During the IPIRG-2 Program (Ref. 4.8), two correction factors, F_{dyn} , defined as the ratio of J-integral at crack initiation from dynamic load to that from quasi-static load, and F_{cyc} , defined as the ratio of J-integral at crack initiation from cyclic load to that from quasi-static load, were developed. From an experimental study on both compact-tension [C(T)] and pipe specimens, it was found that these correction factors are strongly correlated with the yield-to-ultimate strength ratio (σ_y/σ_u) of the pipe material.

For example, Figures 4.22 and 4.23 show the plots of F_{dyn} and F_{cyc} , respectively, as a function of quasi-static yield-to-ultimate strength ratio, σ_y/σ_u , for both austenitic and ferritic materials obtained from both C(T) and pipe specimens. Although, the trend curves of F_{dyn} were different for austenitic and ferritic materials (Figure 4.22), no such difference was found for F_{cyc} (Figure 4.23). Hence, it was assumed that a single curve could be used for determining F_{cyc} , regardless of the material type.

From a linear regression analysis of the test data, several equations were developed for F_{dyn} and F_{cyc} in this study and are shown in Figures 4.22 and 4.23. Although the data and resulting equations were developed for J-integral at crack initiation, it was assumed that the similar correction factor could be applied for the whole quasi-static and monotonic J-R curves. Furthermore, little data on combined cyclic and dynamic loading existed, so it was assumed that there were no synergetic effects on the toughness. With this assumption and using these multipliers (correction factors) to account for the dynamic and cyclic load effects on the fracture toughness properties, a number of probabilistic analyses were performed to calculate the conditional probability of failure for a stainless steel pipe (BWR-1, see Table 4.1) and two carbon steel pipes (BWR-2 and BWR-4, see Table 4.1). The conditional failure probability calculated using both quasi-static and dynamic and cyclic properties are shown in Figures 4.24 through 4.33. The results suggest that the conditional failure probability will increase when the dynamic and cyclic loading effects are included. The magnitude of their resultant effect will, however, depend on the values of F_{dyn} and F_{cyc} which in turn are strongly dependent on the quasi-static yield-to-ultimate strength ratio. In this analysis, the F_{dyn} and F_{cyc} values were based on the mean value of the yield and ultimate strengths for the material of interest. The ratio σ_y/σ_u was not statistically modeled to get a statistically varying F_{dyn} and F_{cyc} due to the limited scope of this project. For the specific pipes, BWR-1, BWR-2, and BWR-4, analyzed in this study, the mean values of quasi-static σ_y/σ_u from Reference 4.8 were 0.35, 0.53, and 0.44 with the corresponding values of total correction factor, $F_{tot} = F_{dyn} \times F_{cyc}$ being 0.63, 0.83, and 0.5, respectively. Since the value of F_{tot} for the BWR-2 pipe was close to 1, the increase of failure probability for that pipe by accounting for dynamic and cyclic loads was not significant. In contrast, the above effects can be significant for BWR-1 and BWR-4 pipes for which cases F_{tot} was significantly lower than 1.

If the BWR-2 pipe were made of A106B instead of A516 Grade 70 carbon steel (see Table 4.1), then the average value of F_{tot} would be 0.5. In that case, the conditional failure probability accounting for dynamic

Figure 4.22 Fracture toughness correction due to dynamic loading

Figure 4.23 Fracture toughness correction due to cyclic loading

Figure 4.24 Conditional failure probability of BWR-1 for dynamic and cyclic loading condition (base metal)

Figure 4.25 Conditional failure probability of BWR-1 for dynamic and cyclic loading condition (weld metal)

Figure 4.26 Conditional failure probability of BWR-1 for dynamic and cyclic loading condition (random crack location)

Figure 4.27 Conditional failure probability of BWR-2 for dynamic and cyclic loading condition (base metal)

Figure 4.28 Conditional failure probability of BWR-2 for dynamic and cyclic loading condition (weld metal)

and cyclic load effects, shown in Figure 4.33, would be much higher. (Note: the failure probabilities in Figure 4.33 were obtained for cracks in base metal only since sufficient weld metal cyclic J-R curve data were unavailable at the time of this analysis.) In summary, the dynamic and cyclic loading rate can affect the prediction of the conditional failure probability for both austenitic and ferritic pipes depending on the quasi-static, yield-to-ultimate strength ratio of the material.

4.4.6 Weld Residual Stresses

The effect of residual stress on crack-opening displacement (COD) in leak-rate analyses is not well understood. There are no simple analyses to account for those effects, and, therefore, they are frequently neglected. During the NRC's "Short Cracks in Piping and Piping Welds" program (Ref. 4.4) and an IPIRG-2 Round-Robin problem (Ref. 4.9) by Brickstad and Moberg of SAQ, a detailed study was performed to assess the magnitude of residual stress effects on crack-opening displacements (Ref. 4.4). Deterministic calculations based on these results were described earlier in this report. This effort examines the effects from a probabilistic viewpoint.

In the Brickstad and Moberg study previous study a residual stress field was calculated using numerical methods and data from an experimental database for two pipe sizes. A thick-wall large-diameter pipe and a thin-wall small-diameter pipe were analyzed using the finite element method. Once a residual stress field was calculated, a pipe flaw evaluation study was performed to study the effects of residual stresses on crack-opening displacements. The crack problem and linear-elastic analysis are reported in Reference 4.4 and were also discussed in detail in the deterministic section of this report. The pipe and crack geometries are defined in Table 4.4. The crack sizes were small enough to be representative of typical situations encountered in leak-before-break analyses.

Table 4.5 shows the calculated values of center COD at the inside, middle, and outside surfaces with and without residual stresses. Also in the study in Reference 4.4, the effects of residual stresses for various applied loads were investigated.

4.4.6.1 Effects of Residual Stresses for Various Applied Loads

At more typical normal operating stresses, which can be much smaller than the applied stresses in Table 4.5, the effects of residual stresses on crack-opening can be much higher. Since the analyses performed in this study are linear-elastic, the effects of residual stresses for pipes with any other applied loads can also be evaluated. This is because the results of finite element analyses conducted for the applied moments in Table 4.5, can be scaled linearly for any other moments or stresses.

Based on linear scaling, Figures 4.34 and 4.35 show the percent change in center COD due to the inclusion of residual stresses for both thicker-wall large-diameter and thinner-wall small-diameter pipes, respectively. In both figures, the horizontal axis defines the applied moment that corresponds to an applied elastic stress as a percentage of ASME Service Level A stress limits. The vertical axis represents the difference of the calculated center COD with residual stresses (δ_{M+RS}) and without residual stresses (δ_M) normalized by the center COD without residual stresses. In general, the effects of residual stresses are significant when the normal operating stresses are lower. For example, when the applied stress is equal to 50-percent of ASME Service Level A stress, the center COD for the thick-walled large diameter pipe will

Table 4.4 Geometric details and applied bending moments for large and small diameter pipes used in finite element analysis

Pipe	Outer Diameter, mm (inch)	Wall Thickness, mm (inch)	θ/π , percent	Bending Moment, kN-m (kip-in)
Thicker-Wall Large-Diameter Pipe	402.6 (15.85)	26.41 (1.04)	12	522.1 ^(a) (4,621)
Thinner-Wall Small-Diameter Pipe	102.0 (4.02)	8.9 (0.35)	20	8.83 ^(b) (78.2)

(a) The corresponding elastic stress = 189.4 MPa (27.5 ksi) ($1.08 \times$ ASME Service Level A limit).

(b) The corresponding elastic stress = 158.2 MPa (22.9 ksi) ($0.9 \times$ ASME Service Level A limit).

Table 4.5 Center-crack-opening displacements calculated with and without residual stresses from finite element analysis

Pipe	Outer Diameter, mm (inch)	Load Case	Center-Crack-Opening Displacement, mm (inch)		
			Inside Surface	Middle Surface	Outside Surface
Thicker-Wall Large-Diameter Pipe	402.6 (15.9)	Bending Moment ^(a) Only	0.274 (0.011)	0.334 (0.013)	0.395 (0.016)
		Bending Moment ^(a) and Residual Stress	0.286 (0.011) (+4.4) ^(b)	0.326 (0.013) (-2.4) ^(b)	0.408 (0.016) (+3.3) ^(b)
Thinner-Wall Small-Diameter Pipe	102.0 (4.02)	Bending Moment ^(c) Only	0.111 (0.004)	0.137 9 (0.0054)	0.164 (0.0065)
		Bending Moment ^(c) and Residual Stress	0.130 (0.0051) (+17.1) ^(b)	0.121 (0.0048) (-11.7) ^(b)	0.112 (0.0044) (-31.7) ^(b)

(a) Moment = 522.1 kN-m (4,621 kip-in) with corresponding elastic stress = 189.4 MPa (27.5 ksi) ($1.08 \times$ ASME Service Level A limit).

(b) Percent change relative to center COD calculated without residual stress (+ = increase; - = decrease).

(c) Moment = 8.83 kN-m (78.2 kip-in) with corresponding elastic stress = 158.2 MPa (22.9 ksi) ($0.9 \times$ ASME Service Level A limit).

increase by 9.46 and 7.11 percent at the inside and outside surfaces, respectively, and decrease by 5.17 percent at the mid-thickness level. Correspondingly, the center COD for the thin-walled small-diameter pipe will increase by 30.9 percent at the inner surface and decrease by 57.2 and 21.1 percent at the outer and middle surfaces of the pipe, respectively. Relative comparisons of the crack-opening results in Figures 4.34 and 4.35 indicate that at any given applied load the residual stress effects are more severe for the thin-walled small-diameter pipe where the residual stress field goes from tension to compression through the thickness than for the thick-walled large-diameter pipe where the residual stress field goes from tension to compression and back to tension through the thickness.

Figure 4.34 Percentage change in calculated COD due to residual stress as a function of applied moment for the thick-walled large-diameter pipe

Figure 4.35 Percentage change in calculated COD due to residual stress as a function of applied moment for the thin-walled small-diameter pipe

According to Figure 4.34, for the thick-walled large-diameter pipe, the calculated CODs which account for residual stresses are always larger than those estimated without residual stresses at both inner and outer surfaces of the pipe. However, the COD at the mid-thickness level can be smaller when the residual stresses are considered. For the thin-walled small-diameter pipe, see Figure 4.35, the calculated COD with residual stresses are higher at the inner surface and lower at the outer or middle surfaces than those without residual stresses. Consequently, the crack-opening area and the subsequent leak-rate calculations can be affected by the residual stresses in those pipes. In particular, when the values of δ_{M+RS} and δ_M are such that $(\delta_{M+RS} - \delta_M) \times 100 / \delta_M$ (the variable in the vertical axis) reaches a value of -100, the calculated COD with residual stresses becomes zero. Hence, there would be no leakage even for a pipe containing a through-wall crack, which clearly demonstrates how important the residual stresses are for the leak-rate calculations. This is especially true for the thin-walled small-diameter pipe in which case the results in Figure 4.35 predict that due to the residual stress, the outer surface of the pipe will close (thus preventing any leakage) when the applied load is equal to 28.6 percent of the ASME Service Level A stress limit. Similar calculations can also be made for the middle surface of both pipes, but the trend curves in Figures 4.34 and 4.35 suggest that for closure to occur, the applied stresses would have to be very small, i.e., 2.60 and 10.5 percent of the ASME Service Level A stress limit for the thicker-wall large-diameter and thinner-wall small-diameter pipes, respectively.

The above results should be viewed as the preliminary estimates for the residual stress effects. Only very limited efforts have been undertaken to determine the accuracy of this approximate finite element analysis compared with more rigorous thermo-plastic finite element analysis, where the residual stress field is numerically simulated, Ref. 4.10. One major difference between these two approaches, is that modeling the residual stress field as a crack-face pressure, imposes a load-controlled stress field, whereas, the actual residual stresses are a local displacement-controlled stress field. The load-controlled crack-face-pressure modeling should produce a larger effect on the COD, than the thermo-plastic modeling of the residual stresses (with unpinning of the nodes to determine the COD). Considering these aspects, the results from this analysis should overemphasize predictions of the crack-opening displacements due to residual stresses. Nevertheless, the conclusion that the residual stress effects on the COD for thinner-wall small-diameter pipe are more severe than the thicker-wall large-diameter pipe, is an important result.

As before, the crack-opening area modules of PSQUIRT were modified to incorporate the effects of weld residual stresses (Figure 4.34 and Figure 4.35). This was done by determining the inside and outside crack opening differences due to the residual stresses, i.e., the crack-face rotation; and accounting for those effects in the SQUIRT leak-rate program. A major assumption was that the length of the crack for leak rate considerations did not change due to the residual stresses. The length change was investigated in the deterministic work described earlier in this report, where it was assumed that the crack-face rotation would have little effect on the leak rate. Accounting for the crack-face rotation in effectively reducing the leakage crack length (i.e., pinching off the flow at the ends of the crack) would require considerably more effort in probabilistic calculations than could be undertaken in this study.

With these assumptions, the PROLBB code was used to determine the conditional failure probability. Table 4.6 shows the calculated mean values of leakage size flaw for the BWR-3 and BWR-5 piping systems (see Table 4.1) for various leak rates and normal operating stress with the effects of residual stresses.

In Table 4.6, it was shown that for the thin-walled pipe case (BWR-5) that for the low operating stresses of 25-percent of Service Level A limits, the residual stresses essentially pinched off the opening at the center

Table 4.6 Mean crack length for various leak rates and normal operating stresses for weld residual stresses

Leak Rate l/min (gpm)	Mean Crack Length, m (inch)			
	Case BWR-3 ^(a)		Case BWR-5 ^(b)	
	Without Residual Stresses	With Residual Stresses	Without Residual Stresses	With Residual Stresses
(a) $\sigma_N/\sigma_{SL-A} = 0.25$				
0.379 (0.1)	0.178 (7.02)	0.181 (7.13)	0.074 (2.92)	(c)
3.79 (1)	0.336 (13.3)	0.335 (13.2)	0.118 (4.66)	(c)
37.9 (10)	0.475 (18.7)	0.479 (18.9)	0.130 (5.12)	(c)
379 (100)	0.511 (20.1)	0.525 (20.7)	0.139 (5.48)	(c)
(b) $\sigma_N/\sigma_{SL-A} = 0.50$				
0.379 (0.1)	0.072 (2.85)	0.065 (2.57)	0.043 (1.69)	0.046 (1.82)
3.79 (1)	0.164 (6.46)	0.150 (5.92)	0.078 (3.07)	0.078 (3.08)
37.9 (10)	0.271 (10.7)	0.251 (9.88)	0.090 (3.53)	0.098 (3.86)
379 (100)	0.324 (12.7)	0.304 (12.0)	0.106 (4.16)	0.112 (4.42)
(c) $\sigma_N/\sigma_{SL-A} = 1.0$				
0.379 (0.1)	0.021 (0.81)	0.018 (0.69)	0.015 (0.59)	0.014 (0.54)
3.79 (1)	0.049 (1.92)	0.041 (1.63)	0.031 (1.20)	0.029 (1.16)
37.9 (10)	0.094 (3.71)	0.085 (3.33)	0.034 (1.35)	0.035 (1.38)
379 (100)	0.110 (4.32)	0.093 (3.67)	0.054 (2.12)	0.055 (2.16)

(a) Case BWR-3 was for a 457.2-mm (18-inch) diameter by 23.9-mm (0.94-inch) thick TP304 stainless steel recirculation branch line.

(b) Case BWR-5 was for a 101.4-mm (4-inch) nominal diameter by 8.51-mm (0.34-inch) thick TP304 bypass line.

(c) Not obtained due to complete crack closure from residual stresses giving a crack length completely around the circumference. Conditional failure probability would be 1.0 in all these cases.

of the crack, hence the crack length was undefined. This undefined crack length could be very large, and effectively the conditional failure probabilities for this case would be 1.0.

Figures 4.36 through 4.39 show the conditional probability of failure with and without residual stresses for these piping systems. These figures show that there is little effect of the residual stresses using the assumption of the crack-face rotation affecting the leakage flow, except of the BWR-3 case at 25-percent of Service Level A where the center of the crack was pinched closed. Recall that in the deterministic analyses discussed earlier in this report, the assumption was that the rotation of the crack faces causes negligible effect on the leak rate. Figures 4.36 to 4.39 support that assumption. This apparent step change in the significance of the residual stresses is due to the assumption of the crack length not being pinched off for leak-rate considerations in the probabilistic analyses. Clearly additional calculations for probabilistic residual stress assessments are needed.

Figure 4.36 Conditional failure probability of BWR-3 with residual stresses (base metal)

Figure 4.37 Conditional failure probability of BWR-3 with residual stresses (weld metal)

Figure 4.38 Conditional failure probability of BWR-5 with residual stresses (base metal)

Figure 4.39 Conditional failure probability of BWR-5 with residual stresses (weld metal)

4.5 Relative Sensitivity Measures For Probabilistic Analyses

For determining the relative importance of the parameters considered in this study, a probability ratio, defined as the ratio of the conditional failure probability considering each parameter to the conditional failure probability of a baseline condition, was calculated. The baseline condition for this sensitivity analysis involved: (1) three different normal operating stresses as percentages of ASME service limits, e.g., $\sigma_N/\sigma_{SL-A} = 0.25, 0.5$, and 1; (2) ASME Service Level B stress limit as the N+SSE stress, (3) an asymmetrically centered crack, (4) unrestrained pressure-induced bending, and (5) quasi-static and monotonic strength and fracture toughness properties. Most results for this baseline condition were originally developed in a previous study by the authors (Ref. 4.1) and were expanded to lower normal operating stress levels and higher N+SSE stresses in Section 4.4.3 of this report.

Figures 4.40 and 4.41 show bar plots of the ratio of conditional failure probability for various parameters accounting for N+SSE stresses (ASME Service Levels C and D stress limits), dynamic and cyclic loading rates, restraint of pressure-induced bending (PIB), and off-centered cracks. Two pipes from Table 4.1, one a stainless steel pipe (BWR-1) and the other a carbon steel pipe (BWR-2), were analyzed to calculate the probability ratio when the leak rate was 3.79 l/min (1 gpm) and the crack was randomly located in either the base or weld metal of the pipe. The results are shown in Figures 4.40 and 4.41 for the BWR-1 and BWR-2 pipe cases, respectively. One general trend from the results of both figures is that the N+SSE stress (particularly, the ASME Service Level D stress), dynamic and cyclic loading rate, and an off-centered crack can increase the conditional failure probability significantly when compared with the failure probability calculated under the baseline condition. The effect due to restraint of pressure-induced bending was marginal. More specifics on the relative significance of these parameters depend on the piping material and the intensity of the normal operating stress.

Figure 4.40 Ratio of conditional failure probability for BWR-1 pipe (stainless steel)

Figure 4.41 Ratio of conditional failure probability for BWR-3 pipe (carbon steel)

Table 4.7 shows the relative ranking of these parameters for various conditions. The parameter which attributed to the largest probability ratio was given a ranking value of 1 and so on. Note that the ranking values were obtained for a stainless steel pipe (BWR-1) and a carbon steel pipe (BWR-2), both being large diameter pipe cases. Further studies are needed to examine these ranking values and determine if they would agree with the results from other piping systems.

Table 4.7 Relative ranking of the parameters based on the probability ratio of CPOF

Parameter	Ranking Value ^(a)		
	$\sigma_N/\sigma_{SL-A} = 0.25$	$\sigma_N/\sigma_{SL-A} = 0.50$	$\sigma_N/\sigma_{SL-A} = 1.0$
(a) Stainless Steel Pipe (BWR-1)			
N+SSE Stress (Service Level C)	2	4	4
N+SSE Stress (Service Level D)	1	1	2
Dynamic and Cyclic Load	3	3	3
Restraint of Pressure-Induced Bending	5	5	5
Off-Centered Crack	4	2	1
(b) Carbon Steel Pipe (BWR-2)			
N+SSE Stress (Service Level C)	2	3	3
N+SSE Stress (Service Level D)	1	2	2
Dynamic and Cyclic Load	4	4	4
Off-Centered Crack	3	1	1

(a) 1 = largest change in CPOF, 4 = negligible change in CPOF.

4.6 References

- 4.1 Rahman, S., Ghadiali, N., Paul, D., and Wilkowski, G., "Probabilistic Pipe Fracture Evaluations for Leak-Rate Detection Applications," NUREG/CR-6004, April 1995.
- 4.2 "Report to the U.S. Nuclear Regulatory Commission Piping Review Committee," Prepared by the Pipe Break Task Group, NUREG/CR-1061, Vol. 3, U.S. Nuclear Regulatory Commission, Washington, D.C., November 1984.
- 4.3 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.
- 4.4 Rahman, S., Brust, F., Ghadiali, N., Choi, Y. H., Krishnaswamy, P., Moberg, F., Brickstad, B., and Wilkowski, G., "Refinement and Evaluation of Crack-Opening Analyses for Short Circumferential Through-Wall Cracks in Pipes," NUREG/CR-6300, April 1995.
- 4.5 Krishnaswamy, P., Scott, P., Mohan, R., Rahman, S., Choi, Y. H., Brust, F., Kilinski, T., Ghadiali, N., Marschall, C., and Wilkowski, G., "Fracture Behavior of Circumferential Short-Surface-Cracked Pipe," NUREG/CR-6298, November 1995.
- 4.6 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.
- 4.7 Wilkowski, G. M., and others, "Short Cracks in Piping and Piping Welds," Semiannual reports by Battelle, NUREG/CR-4599, Vols. 1 to 4, Nos. 1 and 2, U.S. Nuclear Regulatory Commission, Washington, D.C., May 1991 to January 1995.
- 4.8 Wilkowski, G. M., Olson, R., Scott, P., Brust, F., Ghadiali, N., Kilinski, T., Rudland, D., Hopper, A., "The IPIRG Programs - Advances in Pipe Fracture Technology," *Proceedings of LBB'95 Conference*, Lyon France, October 1995.
- 4.9 Rahman, S., Olson, R., Rosenfield, A., and Wilkowski, G., "Summary of Results from the IPIRG-2 Round-Robin Analyses," NUREG/CR-6337, January 1996.
- 4.10 Rahman, S., Dong, P., Wilkowski, G. M., Brickstad, B., Moberg, F., "Effect of Weld Residual Stresses on the Crack-Opening-Area Analysis of Pipes for LBB Applications," *Proceedings of LBB'95 Conference*, Lyon France, October 1995.

5.0 STATISTICAL IN-SERVICE FLAW EVALUATIONS

5.1 Introduction

The application of the in-service flaw evaluations requires: (1) methods for fracture mechanics analyses of a flawed pipe, (2) details of the pipe and crack geometries, and (3) material properties of the pipe. Some of these items are subject to inherent statistical variability. Therefore, a treatment of these uncertainties and an assessment of their impact on "failure" load distribution (based on loads at crack initiation, maximum load from elastic-plastic fracture analyses, and maximum load from limit-load analysis), can be based on theories of statistics and structural reliability.

5.2 In-Service Flaw Evaluation Methodology

The methods available for analyzing the failure behavior of circumferentially surface-cracked pipes can be broadly categorized into; (1) limit-load solutions, (2) design criteria, and (3) J-estimation schemes. Each of these methods involve certain idealizations of crack geometry, material stress-strain behavior and material resistance to crack growth. Details of each of the analysis methods and analysis of circumferential surface-cracked-pipes are described in detail in Reference 5.1.

During the NRC's Short Cracks In Piping and Piping Welds Program (Ref. 5.1) a computer code, NRCPIPES, was developed to analyze surface-cracked pipe under bending and tension loads. A statistical version of this code was developed during this task and was used to perform all the calculations described in this section.

The input parameters to the NRCPIPES code, which exhibit distinct statistical variability, were modeled as random variables. A computer code SIMPIPE was developed during this effort to generate samples of key input parameters. This code was based on algorithms described in Appendix D of Reference 5.2.

5.3 Piping Systems for In-Service Flaw Evaluations

The statistical version of NRCPIPES was applied to the BWR nuclear piping systems for calculating the statistical distribution of crack initiation moments, maximum moments using elastic-plastic fracture mechanics (EPFM), and limit-load moments. A large diameter pipe was selected for these analyses with austenitic and ferritic materials. The piping system included a side riser (BWR-1) and a main steam system (BWR-2). Table 5.1 lists the details of this piping systems.

5.4 Baseline Calculations

The objective of the baseline calculations for the in-service flaw evaluations was to obtain the statistical distribution of failure loads (initiation, EPFM maximum loads, and limit-load maximum moments) using

Table 5.1 BWR piping systems for in-service fracture evaluations

Cases	Piping System	Nominal Diameter, mm (inches)	Thickness, mm (inches)	Base Metal	Weld ^(a) Metal	Assumed ^(b) Cracking Mechanism
(a) Surface cracked BWR Piping Systems						
BWR-1	Side Riser	711.2 (28)	35.8 (1.41)	TP304	SS Flux	IGSCC
BWR-2	Main Steam	711.2 (28)	35.8 (1.41)	A516 Grade 70	CS Flux	Corrosion Fatigue

(a) SS = stainless steel; CS = carbon steel; Flux = submerged arc weld (SAW) or shielded metal arc weld (SMAW).

(b) IGSCC = intergranular stress-corrosion cracking.

deterministic flaw sizes and random quasi-static/monotonic material properties. Since limit-load moments are not dependent on toughness, these values are not reported. There is generally more interest in maximum loads so only those results are presented.

The following flaw sizes were selected for the baseline calculations:

$$a/t = 0.25, 0.50 \text{ and } 0.75$$

$$\theta/\pi = 0.125, 0.250 \text{ and } 0.500$$

where

a = depth of surface flaw
t = pipe wall thickness
 θ = half crack angle.

Piping systems BWR-1 and BWR-2 were evaluated using two J-estimation fracture analysis methods from NRCPIPES. The SC.TNP1 and SC.ENG2 methods were selected. The SC.TNP1 analysis was used because it gave the most accurate predictions of maximum load when compared with pipe experiments in Reference 5.3 when using the L-C oriented toughness. The SC.ENG2 method was used since in Reference 5.1 it was found to be one for the most accurate analyses in comparisons to FEM J values, however, it underpredicted the experimental maximum loads by about 35 percent when using the L-C oriented toughness.

The material for the BWR-1 pipe is TP304 stainless steel and for the BWR-2 pipe system the material was A516 Grade 70 carbon steel. The statistical characteristics (e.g., mean, and covariance) of the stress-strain and J-R curves of TP304 are given in Table 5.2. Table 5.3 gives the similar information for the A516 Grade 70 material. See Reference 5.2 for details of statistical characterizations of pipe material properties. Note that the toughness was in the L-C orientation typically used in circumferential flaw analysis. Ideally the L-R oriented toughness should be used for circumferential surface flaws, but those data are not available. The L-C toughness is typically lower or equal to the L-R toughness. (This toughness difference

Table 5.2 Mean and covariance of quasi-static material properties for TP304 stainless steel base metal at 288 C (550 F)

Random Vector	Mean Vector	Covariance ^(a) Matrix
$\begin{Bmatrix} \sigma_y \\ \sigma_u \end{Bmatrix}^{(b)}$	$\begin{Bmatrix} 154.8 \\ 442.4 \end{Bmatrix}$	$\begin{bmatrix} 124.5 & 33.7 \\ 33.7 & 588.7 \end{bmatrix}$
$\begin{Bmatrix} \alpha \\ n \end{Bmatrix}^{(c)}$	$\begin{Bmatrix} 8.1 \\ 3.8 \end{Bmatrix}$	$\begin{bmatrix} 12.538 & -1.727 \\ -1.727 & 0.308 \end{bmatrix}$
$\begin{Bmatrix} F \\ n \end{Bmatrix}^{(d)}$	$\begin{Bmatrix} 605.3 \\ 3.80 \end{Bmatrix}$	$\begin{bmatrix} 2.80 \times 10^3 & -14.4 \\ -14.4 & 0.308 \end{bmatrix}$
$\begin{Bmatrix} J_{Ic} \\ C \\ m \end{Bmatrix}^{(e)}$	$\begin{Bmatrix} 1243 \\ 344.2 \\ 0.739 \end{Bmatrix}$	$\begin{bmatrix} 3.40 \times 10^5 & 1.11 \times 10^4 & -15.94 \\ 1.11 \times 10^4 & 1.28 \times 10^4 & -1.17 \\ -15.94 & -1.17 & 0.023 \end{bmatrix}$

(a) The components within each vector are correlated and their correlation characteristics are defined by the covariance matrix. The standard deviation vector is the square root of the diagonal terms in the matrix.

(b) Both σ_y and σ_u are in MPa unit.

(c) α and n are dimensionless; $\sigma_o = 154.8$ MPa (22.5 ksi); $E = 182.7$ GPa (26,500 ksi).

(d) F is in MPa unit; n is dimensionless; $E = 182.7$ GPa (26,500); $F = \alpha \epsilon_o / \sigma_o^n$ in original Ramberg-Osgood equation $\epsilon = \sigma/E + \sigma^n/F$.

(e) Both J_{Ic} and C are in kJ/m² unit; m is dimensionless. $J = J_{Ic} + C (\Delta a/r)^m$, where $r = 1$ mm and Δa is in units of mm.

and possible effects from lower constraint in the surface crack than the J-R curve C(T) specimens may explain why the SC.ENG2 method agreed well with the FEM J-integral results but not experimental predictions when using the L-C oriented C(T) specimen J-R curve.) Tables 5.2 and 5.3 were used in generating input to the SIMPIPE computer code and the output from SIMPIPE (1,000 samples) was input to the NRCPIPES code.

The baseline calculations consisted of 18 SIMPIPE runs and 18,000 NRCPIPES runs. The statistical version of NRCPIPES was modified to obtain initiation, EPFM maximum, and limit-load. Only the EPFM maximum load results are presented in this report. Statistical frequency distribution plots were obtained for each of the 18 baseline calculations. A typical frequency distribution plot for the EPFM maximum moment is shown in Figure 5.1. The calculated mean values of maximum moment for each of the baseline calculations are shown in:

- Table 5.4 using the SC.TNP1 analysis for Case BWR-1,
- Table 5.5 using the SC.ENG2 analysis and for Case BWR-1,
- Table 5.6 using the SC.TNP1 analysis and for Case BWR-2, and
- Table 5.7 using the SC.ENG2 analysis and for Case BWR-2.

Table 5.3 Mean and covariance of quasi-static material properties for A516 Grade 70 carbon steel base metal at 288 C (550 F)

Random Vector	Mean Vector	Covariance ^(a) Matrix
$\begin{Bmatrix} \sigma_y \\ \sigma_u \end{Bmatrix}^{(b)}$	$\begin{Bmatrix} 395.3 \\ 562.8 \end{Bmatrix}$	$\begin{bmatrix} 4921 & 530.6 \\ 530.6 & 1098 \end{bmatrix}$
$\begin{Bmatrix} \alpha \\ n \end{Bmatrix}^{(c)}$	$\begin{Bmatrix} 1.89 \\ 5.84 \end{Bmatrix}$	$\begin{bmatrix} 2.41 & -0.837 \\ -0.837 & 0.996 \end{bmatrix}$
$\begin{Bmatrix} F \\ n \end{Bmatrix}^{(d)}$	$\begin{Bmatrix} 892.4 \\ 5.835 \end{Bmatrix}$	$\begin{bmatrix} 1.459 \times 10^4 & -45.79 \\ -45.79 & 0.996 \end{bmatrix}$
$\begin{Bmatrix} J_{Ic} \\ C \\ m \end{Bmatrix}^{(e)}$	$\begin{Bmatrix} 216.194 \\ 204.328 \\ 0.6738 \end{Bmatrix}$	$\begin{bmatrix} 5.189 \times 10^3 & -6.406 \times 10^2 & -1.738 \\ -6.406 \times 10^2 & 5.026 \times 10^3 & -0.240 \\ -1.738 & -0.240 & 0.006 \end{bmatrix}$

(a) The components within each vector are correlated and their correlation characteristics are defined by the covariance matrix. The standard deviation vector is the square root of the diagonal terms in the matrix.

(b) Both σ_y and σ_u are in MPa unit.

(c) α and n are dimensionless; $\sigma_o = 295.3$ MPa (42.8 ksi); $E = 193.1$ GPa (28,000); $F = \alpha \epsilon_o / \sigma_o^n$ in original Ramberg-Osgood equation $\epsilon = \sigma/E + \sigma^n/F$.

(d) F is in MPa unit; n is dimensionless; $E = 193.1$ GPa (28,000).

(e) Both J_{Ic} and C are in kJ/m^2 unit; m is dimensionless. $J = J_{Ic} + C (\Delta a/r)^m$, where $r = 1$ mm and Δa is in units of mm.

Table 5.4 Mean, standard deviation, and COV values of calculated maximum moment for BWR-1 piping system using the SC.TNP1 J-estimation method - baseline calculations

θ/π	a/t	Baseline Calculations - EPFM Maximum Moment		
		Mean, MN-m (kip-in)	Standard Deviation, MN-m (kip-in)	COV
0.125	0.25	4.72 (41,800)	0.352 (3,110)	0.0745
0.250		4.44 (39,300)	0.357 (3,160)	0.0845
0.500		4.25 (37,600)	0.365 (3,140)	0.0858
0.125	0.50	4.43 (39,200)	0.338 (2,990)	0.0763
0.250		3.91 (34,600)	0.289 (2,560)	0.0740
0.500		3.21 (28,400)	0.264 (2,330)	0.0822
0.125	0.75	4.17 (36,900)	0.301 (2,670)	0.0723
0.250		3.35 (29,700)	0.254 (2,250)	0.0758
0.500		2.39 (21,100)	0.213 (1,890)	0.0894

Table 5.5 Mean, standard deviation, and COV values of calculated maximum moment for BWR-1 piping system using the SC.ENG2 J-estimation method - baseline calculations

θ/π	a/t	Baseline Calculations - EPFM Maximum Moment		
		Mean, MN-m (kip-in)	Standard Deviation, MN-m (kip-in)	COV
0.125	0.25	5.16 (45,700)	0.440 (3,900)	0.0853
0.250		4.85 (43,000)	0.427 (3,800)	0.0880
0.500		4.53 (40,100)	0.409 (3,620)	0.0903
0.125	0.50	3.51 (31,000)	0.309 (2,730)	0.0881
0.250		3.09 (27,400)	0.282 (2,490)	0.0911
0.500		2.68 (23,800)	0.251 (2,220)	0.0934
0.125	0.75	2.23 (19,700)	0.209 (1,850)	0.0941
0.250		1.79 (15,900)	0.172 (1,520)	0.0961
0.500		1.39 (12,300)	0.134 (1,190)	0.0963

Table 5.6 Mean, standard deviation, and COV values of calculated maximum moment for BWR-2 piping system using the SC.TNP1 J-estimation method - baseline calculations

θ/π	a/t	Baseline Calculations - EPFM Maximum Moment		
		Mean, MN-m (kip-in)	Standard Deviation, MN-m (kip-in)	COV
0.125	0.25	6.65 (58,800)	0.619 (5,480)	0.0931
0.250		6.20 (54,800)	0.581 (5,140)	0.0938
0.500		5.83 (51,600)	0.537 (4,750)	0.0921
0.125	0.50	6.51 (57,600)	0.622 (5,500)	0.0955
0.250		5.55 (49,100)	0.515 (4,550)	0.0927
0.500		4.64 (41,100)	0.438 (3,800)	0.0944
0.125	0.75	5.78 (51,100)	0.527 (4,660)	0.0912
0.250		4.68 (41,500)	0.429 (3,800)	0.0916
0.500		3.28 (29,100)	0.305 (2,700)	0.0929

Table 5.7 Mean, standard deviation, and COV values of calculated maximum moment for BWR-2 piping system using the SC.ENG2 J-estimation method - baseline calculations

θ/π	a/t	Baseline Calculation - EPFM Maximum Moment		
		Mean, MN-m (kip-in)	Standard Deviation, MN-m (kip-in)	COV
0.125	0.25	6.89 (61,000)	0.613 (5,420)	0.0889
0.250		6.44 (57,000)	0.574 (5,080)	0.0892
0.500		5.96 (52,800)	0.535 (4,740)	0.0897
0.125	0.50	4.81 (42,600)	0.437 (3,870)	0.0908
0.250		4.21 (37,300)	0.386 (3,410)	0.0915
0.500		3.62 (32,100)	0.335 (2,960)	0.0924
0.125	0.75	3.05 (27,000)	0.287 (2,630)	0.0941
0.250		2.42 (21,400)	0.232 (2,050)	0.0958
0.500		1.85 (16,400)	0.181 (1,600)	0.0977

Figure 5.1 Histogram of maximum moment of BWR-1 pipe for in-service analysis (QS material properties at 288 C (550 F), SC.TNP1 method, $a/t = 0.25$, $\theta/\pi = 0.125$)

5.5 Effects of Dynamic & Cyclic Loading Rates

The objective of this effort was to determine the effects of dynamic and cyclic loading rates for a seismic event on probability distribution of failure loads (initiation, EPFM maximum, and limit-load moments). Only the EPFM maximum load value are given in this report. Since limit-load moments are not dependent on toughness, these values are not reported. There is generally more interest in maximum loads, so only these results are presented. The baseline calculations described above were re-analyzed by modifying the J-R curve as follows :

$$J_{\text{seismic}} = J_{\text{QS}} F_{\text{dyn}} F_{\text{cyc}} \quad (5-1)$$

where

- J_{seismic} = J value under dynamic and cyclic loading,
- F_{dyn} = Ratio of dynamic J_{Ic} to quasi-static J_{Ic} ,
- F_{cyc} = Ratio of cyclic J_{Ic} (for $R = -1$ loading) to quasi-static J_{Ic} , and
- J_{QS} = J value under quasi-static monotonic loading (i.e., typical ASTM value).

The correction factors used to account for seismic loading on the J-R curves were described in detail in Section 4.5.5 of this report. These corrections are a function of the yield to ultimate strength ratio, but in this analysis the F_{dyn} and F_{cyc} values were determined using the mean yield and ultimate strengths for the material of interest. The baseline calculations with the seismic adjusted J-R curve were analyzed. Frequency distribution plots were obtained for each of the 18 calculations. A typical plot for maximum moment is shown in Figure 5.2. The calculated mean and standard deviation values of maximum moment for each of the calculations are shown in:

- Table 5.8 using the SC.TNP1 analysis for Case BWR-1,
- Table 5.9 using the SC.ENG2 analysis for Case BWR-1,
- Table 5.10 using the SC.TNP1 analysis for Case BWR-2, and
- Table 5.11 using the SC.ENG2 analysis for Case BWR-2.

To assess the significance of these results, the ratio of the mean maximum loads of the cyclic/dynamic analyses to the baseline calculations using the quasi-static monotonic toughness properties were determined for all flaw sizes for both pipes and both analyses. Interestingly, the ratio of the maximum loads were essentially constant for all flaw geometries and depended only on the pipe case analyzed. For instance, for Case BWR-1 (711.2-mm [28-inch] diameter TP304 stainless steel pipe base metal crack case), the ratio of the mean cyclic/dynamic toughness corrected maximum load to the baseline maximum loads was 0.905. For Case BWR-2 (711.2-mm [28-inch] diameter A516 Grade 70 carbon steel pipe base

Figure 5.2 Histogram of maximum moment of BWR-1 pipe for in-service analysis (dynamic and cyclic loading effects on material properties at 288 C (550 F), SC.TNP1 method, $a/t = 0.25$, $\theta/\pi = 0.125$)

Table 5.8 Mean, standard deviation, and COV values of calculated maximum moment for BWR-1 piping system using the SC.TNP1 J-estimation method - effect of seismic loading on toughness

θ/π	a/t	Effect of cyclic/dynamic loading on toughness calculations of maximum moment		
		Mean, MN-m (kip-in)	Standard Deviation, MN-m (kip-in)	COV
0.125	0.25	4.27 (37,800)	0.367 (3,250)	0.0860
0.250		4.02 (35,600)	0.367 (3,250)	0.0914
0.500		3.85 (34,000)	0.373 (3,300)	0.0969
0.125	0.50	4.01 (35,500)	0.366 (3,240)	0.0912
0.250		3.54 (31,300)	0.304 (2,690)	0.0859
0.500		2.91 (25,700)	0.277 (2,450)	0.0954
0.125	0.75	3.78 (33,400)	0.319 (2,830)	0.0845
0.250		3.04 (26,900)	0.269 (2,380)	0.0886
0.500		2.16 (19,100)	0.219 (1,940)	0.1014

Table 5.9 Mean, standard deviation, and COV values of calculated maximum moment for BWR-1 piping system using the SC.ENG2 J-estimation method - effect of seismic loading on toughness

θ/π	a/t	Effect of cyclic/dynamic loading on toughness calculations of maximum moment		
		Mean, MN-m (kip-in)	Standard Deviation, MN-m (kip-in)	COV
0.125	0.25	4.66 (41,300)	0.452 (4,000)	0.0969
0.250		4.39 (38,800)	0.436 (3,860)	0.0994
0.500		4.10 (36,300)	0.417 (3,690)	0.1017
0.125	0.50	3.17 (28,000)	0.320 (2,830)	0.1009
0.250		2.79 (24,700)	0.290 (2,560)	0.1036
0.500		2.43 (21,500)	0.257 (2,270)	0.1057
0.125	0.75	2.01 (17,800)	0.215 (1,900)	0.1067
0.250		1.62 (14,300)	0.176 (1,550)	0.1084
0.500		1.26 (11,100)	0.136 (1,210)	0.1083

metal crack case), the ratio of the mean cyclic/dynamic toughness corrected maximum load to the baseline maximum loads was 0.960. These ratios were essentially constant for all pipe sizes and the same for both the SC.TNP1 and SC.ENG2 analyses. The uniqueness of a single number for all surface-cracked pipe flaw geometries is consistent with Z-factor calculations using the SC.TNP1 analysis in Reference 5.1.

These results suggest that the effect of cyclic/dynamic loading on the maximum loads was about 4 and 10 percent in the A516 Grade 70 and TP304 cases, respectively. Interestingly, from the separate deterministic analysis conducted on the TP304 stainless steel pipe with the same pipe diameter, this ratio from Figure 3.2 the reduction in maximum loads was about 20 percent. This appears to be an inconsistency, so

Table 5.10 Mean, standard deviation, and COV values of calculated maximum moment for BWR-2 piping system using the SC.TNP1 J-estimation method - effect of seismic loading on toughness

θ/π	a/t	Effect of cyclic/dynamic loading on toughness calculations of maximum moment		
		Mean, MN-m (kip-in)	Standard Deviation, MN-m (kip-in)	COV
0.125	0.25	6.38 (56,400)	0.877 (7,760)	0.1374
0.250		5.94 (52,600)	0.818 (7,240)	0.1375
0.500		5.59 (49,500)	0.770 (6,820)	0.1377
0.125	0.50	6.24 (55,300)	0.876 (7,750)	0.1402
0.250		5.33 (47,200)	0.727 (6,430)	0.1364
0.500		4.45 (39,400)	0.617 (5,460)	0.1386
0.125	0.75	5.54 (49,100)	0.756 (6,690)	0.1363
0.250		4.50 (39,800)	0.609 (5,390)	0.1355
0.500		3.15 (27,900)	0.431 (3,810)	0.1366

Table 5.11 Mean, standard deviation, and COV values of calculated maximum moment for BWR-2 piping system using the SC.ENG2 J-estimation method - effect of seismic loading on toughness

θ/π	a/t	Effect of cyclic/dynamic loading on toughness calculations of maximum moment		
		Mean, MN-m (kip-in)	Standard Deviation, MN-m (kip-in)	COV
0.125	0.25	6.57 (58,100)	0.953 (8,440)	0.1452
0.250		6.13 (54,300)	0.892 (7,900)	0.1454
0.500		5.68 (50,300)	0.829 (7,340)	0.1459
0.125	0.50	4.57 (40,500)	0.670 (6,100)	0.1509
0.250		4.00 (35,400)	0.605 (5,350)	0.1510
0.500		3.45 (30,500)	0.520 (4,600)	0.1510
0.125	0.75	2.90 (25,700)	0.440 (3,890)	0.1515
0.250		2.30 (20,400)	0.358 (3,080)	0.1514
0.500		1.76 (15,660)	0.266 (2,350)	0.1509

the initial inputs were examined more closely. The review showed that the deterministic analyses used a stainless steel pipe that had a yield strength close to the ASME Section II values at 288 C (550 F). This pipe had a lower yield-to-ultimate strength ratio than the average statistical value cited in Table 5.2.

Hence, the dynamic/cyclic toughness correction was a lower number in the deterministic analyses than the statistical analyses. Consequently, the predicted maximum loads from deterministic analyses were smaller than those from statistical analyses. Also, to improve the current statistical analysis in the future, the yield-to-ultimate strength ratios need to be statistically determined (with the proper cross-correlation coefficients between yield and ultimate strengths readily available in Table 5.2) and then used to determine a statistical-

based toughness correction factor due to cyclic and dynamic effects, rather than keeping this correction as a constant.

Similarly, the A516 Grade 70 results showed an even lower effect from the dynamic and cyclic effects on toughness, because the yield-to-ultimate strength ratio from Table 5.3 was much higher than the stainless steels. Hence, the toughness correction was less severe and the maximum loads did not change as much.

The results from the deterministic and statistical analyses show that the effect of the yield-to-ultimate strength ratio is an important parameter in determining the corrections to the toughness and the subsequent effects on the load-carrying capacity.

The coefficient of variance (COV), which is the ratio of the standard deviation to the mean, were about 8 and 9 percent for the stainless steel base metal for the baseline and cyclic/dynamic cases, respectively. For the A516 Grade 70 base metal case, the COV values were from 9 to 15 percent for the baseline and cyclic/dynamic cases, respectively. The COV values may have changed more from the baseline to the cyclic/dynamic cases if rather than using a deterministic constant correction factor on the toughness, the correction factor was statistically varied with the yield-to-ultimate strength ratio.

5.6 Effects of Crack Depth Measurement Uncertainty

The objective of this effort was to determine the effects of uncertainty in crack depth measurements on the frequency distribution of failure loads (initiation, EPFM maximum, and limit-load maximum moments). The baseline calculations described in Section 5.4 were re-analyzed with the crack depth treated as a random variable rather than being deterministic. Since limit-load moments are not dependent on toughness, these values are not reported. There is generally more interest in maximum loads, so only those results are presented.

The accuracy of surface-crack depth measurements was studied in an NDE round-robin effort by EPRI in 1989 (Ref. 5.4). Figure 5.3 shows the results of that effort by 17 teams in the NDE round robin. This figure shows the average (true depth/pipe wall thickness) versus (ultrasonic measured depth/pipe wall thickness) for each of the 17 teams of inspectors. The ideal case, where there are no errors in measurement, is also shown in Figure 5.3. These data were then used to generate statistical results of depth/thickness ratio (mean, covariance, and standard deviation) as part of this project. Figure 5.4 shows plots of the statistical results.

In Figure 5.4 it is interesting to note that from a comparison of the mean curve and the 1:1 line, for flaw depths less than 35 percent of the thickness, the 1989 UT results overestimated the depth and consequently the load-carrying capacity is actually higher. For flaw depths greater than 35 percent of the thickness, the 1989 UT results underestimated the flaw depth and consequently the predicted load-carrying capacity would be less than the actual capacity. It is also interesting to note in Figure 5.4 that the COV is very high for flaw depths less than 35 percent of the thickness, then becomes relatively constant at larger flaw sizes. The COV of 40 to 80 percent of the mean is much larger than the maximum load COV due to statistic change in material properties in the baseline calculations.

Figure 5.3 Results of 1989 EPRI IGSCC Round Robin on crack depth sizing (17 teams, linear regression lines)

Figure 5.4 Statistical results of depth/thickness ratio from the 1989 EPRI UT Round-Robin data

This statistical variation was incorporated into the input data for SIMPIPE where now the crack size a/t was treated as a random variable. Frequency distribution plots were obtained for each of the 18 (9 flow geometries x 2 analysis methods) calculations. A typical frequency distribution plot for maximum moment is shown in Figure 5.5. The calculated mean, standard deviation, and COV values of the EPFM maximum moment for each of the calculations are shown in Table 5.12 (using the SC.TNP1 analysis for case BWR-1) and Table 5.13 (using the SC.ENG2 analysis for case BWR-1). The 1989 EPRI UT data on crack depth measurements was only for stainless steel material, hence the statistical in-service flaw evaluation analysis was conducted for the TP304 BWR-1 piping system case and not for the A516 Grade 70 BWR-2 piping system case. No carbon steel piping UT flaw sizing accuracy data were readily available.

To assess the relative effects of these results, a graph was made of the ratio of the mean values of the maximum moments of the pipes with the actual flaw size to the pipes with the UT sized flaw, i.e., the ratio of Tables 5.4 and 5.5 mean values to Tables 5.12 and 5.13 mean values. This graph is shown in Figure 5.6. A ratio of greater than 1.0 means that the UT flaw depth was actually smaller and hence the maximum load-carrying capacity was greater. The results in Figure 5.6 show that the UT flaw size moment (M_{UT}) is slightly greater for the a/t values of less than 0.35, and then decreases for larger a/t values. This is consistent with the trend illustrated in Figure 5.4 when comparing the mean UT flaw size line to the 1:1 line, i.e., there is agreement for a/t values of 0.35.

The magnitude of the moment ratios differs with the flaw a/t and θ/π values as well as by the J-estimation scheme used. The SC.TNP1 analysis method which has been found to give the best agreement with experimental maximum loads, shows that the maximum underprediction of the actual loads range from a few percent (for short flaws of any depth) to an overprediction of about 35 percent for the deepest and longest flaw evaluated.

A comparison of the COV values in Tables 5.12 and 5.13 shows that the COV values increase slightly as the flaw depths and lengths get larger. This is expected since the input data have more uncertainty due to randomness of the crack depth. The COV values were also compared to the COV values for the baseline calculations where in the baseline case varied just the quasi-static material properties. This comparison showed that the COV of the UT flaw sizing case was 1.6 to 2.4 larger than the baseline COV when using the SC.TNP1 analysis, and 2.7 to 4.0 larger than the baseline COV when using the SC.ENG2 analysis method.

It is also helpful to see a graphical representation of the statistical bounds in the EPFM maximum moment predictions due to the UT flaw sizing. These bounds are presented in Figure 5.7 which are presented in a similar manner to Figure 5.6, however, in this case the mean $\pm$ one standard deviation bounds are drawn. To make this figure easier to read, (a) only the SC.TNP1 results are given, since the SC.TNP1 analysis is the most accurate in predicting maximum moments, and (b) only the θ/π values of 0.125 and 0.5 are given since they represent the extremes of the crack lengths investigated. In Figure 5.7a, the ordinate is the $M_{baseline}(\text{mean value})/M_{UT}(\text{mean} \pm \text{one standard deviation})$. This figure shows the $\pm$ one standard deviation bounds of the maximum moment predictions due to the UT flaw sizing uncertainty *and* the uncertainty due to statistical scatter of material properties. Figure 5.7b shows a similar graph, but in this case the numerator in the ordinate is the baseline maximum moments with the corresponding $\pm$ one standard deviation due to the material property changes. This figure gives the $\pm$ one standard deviation bounds of the maximum load predictions due *only* to the UT flaw sizing uncertainty. The bounds are much smaller in Figure 5.7b. In comparing these two figures, it can be seen that the bounds for the θ/π value of 0.125

Figure 5.5 Histogram of maximum moment of BWR-1 pipe for in-service analysis (QS material properties at 288 C (550 F), SC.TNP1 method, $a/t = 0.25$, $\theta/\pi = 0.125$, crack depth uncertainty effects)

Table 5.12 Mean, standard deviation, and COV values of calculated maximum moment for BWR-1 piping system using the SC.TNP1 J-estimation method - effect of flaw sizing variability

Effect of flaw sizing variability on calculation of EPFM maximum moment				
θ/π	a/t	Mean, MN-m (kip-in)	Standard Deviation, MN-m (kip-in)	COV
0.125	0.25	4.64 (41,100)	0.575 (5,090)	0.124
0.250		4.39 (38,900)	0.616 (5,450)	0.140
0.500		4.05 (35,900)	0.814 (7,210)	0.201
0.125	0.50	4.38 (38,800)	0.484 (4,290)	0.111
0.250		4.10 (36,300)	0.499 (4,410)	0.122
0.500		3.59 (31,800)	0.695 (6,150)	0.194
0.125	0.75	4.24 (37,600)	0.469 (4,150)	0.111
0.250		3.91 (34,600)	0.504 (4,460)	0.129
0.500		3.26 (28,900)	0.690 (6,116)	0.212

Table 5.13 Mean, standard deviation, and COV values of calculated maximum moment for BWR-1 piping system using the SC.ENG2 J-estimation method effect of flaw sizing variability

θ/π	a/t	Effect of flaw sizing variability on calculation of maximum moment		
		Mean, MN-m (kip-in)	Standard Deviation, MN-m (kip-in)	COV
0.125	0.25	4.82 (42,700)	1.138 (10,600)	0.236
0.250		4.50 (39,800)	1.199 (10,600)	0.267
0.500		4.17 (36,900)	1.252 (11,100)	0.301
0.125	0.50	4.14 (36,700)	1.039 (9,190)	0.251
0.250		3.77 (33,400)	1.079 (5,550)	0.286
0.500		3.40 (30,100)	1.107 (9,790)	0.325
0.125	0.75	3.62 (32,100)	1.052 (9,310)	0.290
0.250		3.23 (28,600)	1.087 (9,620)	0.337
0.500		2.84 (25,100)	1.094 (9,690)	0.385

Figure 5.6 Ratio of mean maximum moment ($M_{baseline}$) to mean maximum moment for uncertainty in crack depth measurement (M_{UT})

(a) $M_{\text{baseline}}(\text{Mean}) / M_{\text{UT}}(\text{Mean} \pm \text{Std. Dev.})$

(b) $M_{\text{baseline}}(\text{Mean} \pm \text{Std. Dev.}) / M_{\text{UT}}(\text{Mean} \pm \text{Std. Dev.})$

Figure 5.7 Ratio of baseline (actual) maximum moments to maximum moments from UT flaw size

are more sensitive to the material property changes than the UT flaw sizing accuracy. The maximum moments are more sensitive to the UT flaw sizing accuracy with the longer flaw case of $\theta/\pi = 0.5$.

The COV for UT sizing variability (with quasi-static material properties varied) also showed a greater change in the COV than from varying just the toughness for cyclic/dynamic effects. As previously noted, the variability due to cyclic/dynamic effects was accomplished by taking a toughness correction factor on the average yield-to-ultimate strength ratio, rather than varying this toughness correction with yield-to-ultimate strength ratios statistically. Hence, the variability due to dynamic and cyclic effects may be higher than calculated in this effort. Calculations combining the variability of the UT sizing and the dynamic and cyclic effects were not made in this effort.

Finally, it is also interesting to note that the flaw depth COV was much larger than the COV for the resulting maximum loads. For instance, the COV of the flaw depth sizing was 40 to 80 percent of the mean, depending on the a/t value. The larger COV was for *smaller* a/t values. However, the maximum load COV values were 11 to 22 percent with the SC.TNP1 analysis and 24 to 38 percent with the SC.ENG2 analysis method with the higher COV values being for *deep* and long flaws.

The unexpected trend, of larger COV values for sizing small a/t flaws from the 1989 EPRI UT round-robin results versus the maximum load COV values being larger for deep flaws, comes from the fact that the maximum loads are more sensitive to deep flaws than shallow flaws. This is illustrated in Figure 5.8 which shows predictions of maximum loads for a variety of a/t and θ/π values for the BWR-1 pipe case (using the mean quasi-static material properties). Figures 5.8a, 5.8b, and 5.8c correspond to predicted maximum moments using the Net-Section-Collapse analysis, the SC.TNP1 analysis, and the SC.ENG2 analysis, respectively. Note that for small θ/π values, the maximum moments are very insensitive to crack depth, especially in the Net-Section-Collapse and SC.TNP1 analysis, hence a large COV on flaw size will result in a small COV on maximum load. The SC.ENG2 analysis shows a greater degree of sensitivity to short flaw depth, which comes from implementing the Kurihara modification (Ref. 5.5) of the Net-Section-Collapse analysis. The Kurihara modification is an empirical modification on the Net-Section-Collapse analysis that attempted to correct the Net-Section-Collapse analysis based on observations from pipe tests with short deep cracks failing below the Net-Section-Collapse predicted loads. However, Reference 5.1 showed that this Kurihara modification over corrects for flaws greater than 25 percent of the circumference. Hence the SC.ENG2 COV values are probably too high for the longer flaws, and the SC.TNP1 COV values are more realistic for flaw lengths greater than 25 percent of the circumference. The SC.ENG2 analysis COV values may be better than the SC.TNP1 COV values for flaw lengths shorter than 25 percent of the circumference.

(a) NSC BWR-1

(b) SC.TNP1 BWR-1

Figure 5.8 Limit moments and allowable moments for BWR-1 piping system

(c) SC.ENG2 BWR-1

Figure 5.8 (Continued)

5.7 References

- 5.1 Krishnaswamy, P., Scott, P., Mohan, R., Rahman, S., Choi, Y. H., Brust F., Kilinski, T., Ghadiali, N., Marschall, C., and Wilkowski, G., "Fracture Behavior of Circumferential Short-Surface-Cracked Pipe," NUREG/CR-6298, November 1995.
- 5.2 Rahman, S., Ghadiali, N., Paul, D., and Wilkowski, G., "Probabilistic Pipe Fracture Evaluations for Leak-Rate Detection Applications," NUREG/CR-6004, April 1995.
- 5.3 Wilkowski, G. M., Olson, R., Scott, P., Brust, F., Ghadiali, N., Kilinski, T., Rudland, D., Hopper, A., "The IPIRG Programs - Advances in Pipe Fracture Technology," *Proceedings of LBB'95 Conference*, Lyon France, October 1995.
- 5.4 "Accuracy of Ultrasonic Flaw Sizing Techniques for Reactor Pressure Vessels," Electric Power Research Institute, EPRI-NP-6273, Palo Alto, California, 1989.
- 5.5 Kurihara, R., Ueda, S., and Sturm, D., "Estimation of the Ductile Unstable Fracture of Pipe with a Circumferential Surface Crack Subjected to Bending," *Nuclear Engineering and Design*, Vol. 106, pp. 265-273, 1988.

6.0 SUMMARY AND CONCLUSIONS

The following summarizes the objectives, results, and presents conclusions from both the deterministic and probabilistic studies. These results are organized by the technical aspect being evaluated. In most cases there are both deterministic and probabilistic results. The deterministic analyses were conducted independently of the probabilistic analysis, which offered the opportunity to validate conclusions from each of these studies. At the end of this section, the relative significance of these various technical aspects are ranked.

6.1 Uncertainty Analyses Relative to LBB

6.1.1 Evaluation of Different Crack Morphology Default Values

The objective in this effort was to assess any differences in default crack morphology parameters between PICEP Version 4.0, SQUIRT Version 2.3, and SQUIRT Version 2.4. This evaluation was done only in a deterministic manner. The SQUIRT Version 2.3 default values were typical values from an experiment, but no special attempt was made to define these values. The basis of the PICEP default values is not well documented. It is believed that the IGSCC values come from an EPRI/Battelle leak-rate program, but the basis of the corrosion-fatigue crack morphology default values could not be traced. The SQUIRT 2.4 default crack morphology parameters are the mean values from statistical evaluation of cracks removed from service as documented in Reference 6.1. The crack morphology parameters in these analyses are the surface roughness and the number of turns per unit of thickness. Cracks resulting from both IGSCC and corrosion-fatigue mechanisms were considered.

The calculated crack lengths using both SQUIRT versions and PICEP Version 4.0 default crack morphology parameters for IGSCC cracks were very close. However, it should be noted that the default values are mean values and there is a high statistical variability for IGSCC crack morphology parameters. This is due to the fact that typical wrought stainless steel piping products will have grain sizes of 3 to 6^(*). Using ASTM Standard E112 to convert grain size numbers to physical dimensions gives a range of grain sizes that vary by a factor of 2.77 in average "diameter". Consequently, roughness and number of turns will vary proportionally to this change in grain size for an IGSCC.

The calculated crack lengths for a given leak rate using the corrosion-fatigue crack default parameters had a larger difference. These resulted in a 16 to 40 percent larger crack length, for the 18.9 liters per minute (5 gpm) leak, when using SQUIRT 2.3 default parameters compared with PICEP or SQUIRT 2.4. The new SQUIRT Version 2.4 statistical default values resulted in crack lengths of 4 to 12 percent larger values than crack lengths calculated using the PICEP default values at the 18.9 liters per minute (5 gpm) leak rate. All of these differences were less for the 1.89 l/min (0.5 gpm) leak.

* Information from Dr. H. Mehta of GE Nuclear Energy Operations.

6.1.2 Evaluation of COD Dependant and Independent Crack Morphology Models for Tight Crack Leak Rate Analyses

The objective of this analysis was to assess the effect of using a COD-dependent crack morphology model to alleviate problems in leak-rate calculations for tight cracks. This analysis was only conducted in a deterministic manner.

One of the major concerns with tight cracks comes from limitations on the friction factor equations used in all leak-rate codes. Experimental data exist over a limited range for these friction factor equations, and extrapolating them to lower values was found to give erroneous numerical solutions. In fact, at low COD values a critical term in the friction-factor equation becomes negative which contributes to the leak-rate going to zero and then increasing again with even smaller COD values. The COD-dependant crack morphology model circumvents the friction-factor problem by increasing the number of turns in the flow path and reducing the surface roughness, i.e., the local roughness is that of a grain boundary for an IGSCC. By doing this, it was found that the COD-dependent crack-morphology model gave leak rates which could be a factor of four below the standard COD-independent crack-morphology models when the friction-factor equation lower limit was reached.

For the case studied, the improved model gave the same results as the standard model for leak rates greater than 7.56 l/min (2 gpm), had some improvement for leak rates from 4.5 to 3.5 l/min (0.93 to 1.2 gpm), and gave very significantly different results for leak rates less than 3.5 l/min (0.93 gpm). These leak rates are within the range of leak rates of interest for LBB analyses, but without the safety factor of 10 used in the NRC SRP 3.6.3. If deterministic calculations are made with the safety factor of 10 on leak rates for LBB analyses, then the current COD-independent models and the COD-dependent model give the same result. If the safety factor is not used in the analyses (i.e., in probabilistic analyses), or much lower leak rates are of interest, then the new COD-dependent crack-morphology model can provide more accurate leak-rate predictions.

6.1.3 Changes of Normal Operating and N+SSE Stress Levels on Failure Probability

LBB analyses could be conducted with either a conservative deterministic analysis, or with a probabilistic analysis. Deterministic LBB analyses typically involve a two-step calculation. First, a flaw size corresponding to a given leak rate (with some safety factor on the leak rate) is determined at the normal operating stresses. The second step is that flaw size is used to calculate if the flaw is stable at the expected SSE loads. In this second step, safety factors may be applied to either the leaking crack length or the SSE loads, and the worst case material properties are used.

There are many variables in an LBB analysis and some of these have some statistical variation. To assess this variability on the likelihood of a failure, probabilistic analyses were conducted. The term conditional probability of failure (CPOF) is frequently used in probabilistic work and means two things. First, there are some important conditions or assumptions in the analysis. For example, in the calculations conducted in this report, it was assumed that an SSE load would occur with a probability of 1.0. The probability of occurrence of an SSE event occurring is a site-specific probability. If this probability is known it could be multiplied times our CPOF value to obtain the failure probability in more absolute terms, i.e., to "decondition" the analysis. Another condition is that the leakage at a given rate would occur and be

detected with a probability of 1.0. Subcritical crack growth analyses could be conducted to determine the probability of a leaking crack occurring, and the reliability of the leak detection equipment could be determined. Again, this probability times our CPOF would further "decondition" the analyses. In reality, all probabilistic analyses are conditional or have some inherent assumptions in the deterministic analyses or analysis logic.

The second important aspect of the term CPOF is the meaning of the word failure. The failure criterion in this probabilistic analysis is normal plus SSE stresses equaling or exceeding the maximum load-carrying capacity of the through-wall-cracked pipe. This is consistent with the U.S. NRC LBB analysis procedures. In actuality, if some of these stresses are secondary stresses, it is possible that the cracked pipe may not experience a double-ended guillotine break failure, i.e., there will only be a large leak. Also if the stresses are dynamic and cyclic, the crack may start to grow, but the cyclic loads may decrease fast enough to arrest unstable crack growth. Additionally, the analyses use elastic uncracked pipe stress values, where the presence of the crack and plasticity from the crack may truncate the magnitude of the seismic moments. In the present work, a plasticity correction factor was used to reduce the elastic pipe stresses (Ref. 6.1). All of these considerations could be used to refine the deterministic "failure" analyses and hence to further "decondition" the CPOF values in the analyses. This would make the CPOF values more realistic. Unfortunately, such improvements make the probabilistic model much more complicated than could be undertaken within the scope of this analysis.

With the above limitations on the conditions and definitions of "failure" in mind, the objective of this study was to expand the original probabilistic results from Reference 6.1 to a larger range of normal operating stresses as well as a larger range of N+SSE stresses. This was only a probabilistic study, where the safety factors on leak rate, crack length, and SSE loads were not used because their uncertainties were explicitly modeled by probability distributions. Additionally, these CPOF values were used as baseline values for additional probabilistic analyses where the importance of other technical issues were evaluated in this report.

From the results of the probabilistic analysis of ten different PWR and BWR piping systems, it was found that the normal operating stresses have a more profound effect on the conditional probability of failure than the level of the SSE stresses in the LBB analyses. Any uncertainty in the normal operating stresses can lead to large variations in the predicted conditional probability of failure. The change in the failure probabilities for normal operating stresses from 0.25 to 1.0 of Service Level A limits was 6 to 9 orders of magnitude (depending on the leak rate).

For a fixed normal operating stress, any increase in the N+SSE stress above the ASME Service Level B maximum limits to ASME Service Levels C or D maximum limits will also increase the conditional probability of failure, but these changes in the conditional probability of failure were much smaller (up to 3 orders of magnitude change) than changes due to the normal operating stresses. At first it was expected that the CPOF values of the Service Level D N+SSE stresses should approach 1.0. However, due to the use of actual strengths (rather than Code Strengths) and a correction for plasticity on the elastic stresses, the Service Level D CPOF values were much lower.

One major implication from this work is that the requirement for conducting an LBB analysis may need to be reconsidered. For instance, NRC SRP 3.6.3 for LBB analyses requires the plant operator to "Identify the location(s) at which the highest stresses coincident with the poorest material properties occur..." It was

shown that the conditional failure probabilities are the highest when the normal operating stresses are low, and that a location with lower N+SSE stresses and low normal operating stresses would have a higher failure probability than a location with higher normal operating stresses and higher N+SSE stresses. Hence, the LBB criterion needs to be more specific on what stresses are the highest, i.e., either the normal operating stresses or normal plus SSE stresses. Since a crack is more likely to occur with high normal operating stresses, one logical choice is to select the locations with the highest normal operating stresses for LBB analyses.

With regard to the current LBB procedure, it is somewhat counterintuitive to good design practice that one can more readily satisfy LBB if the normal operating stresses are high. Good design practice would like to have low normal operating stresses so that cracking problems do not occur and one would have a safer plant. Yet with the low normal operating stresses, it is difficult to satisfy LBB because for the typical LBB leak rates, the crack size is very large at low normal operating stresses. As a result, satisfying the fracture criterion at normal plus SSE loads is difficult. A basic philosophy difference exists here that is not easy to reconcile.

6.1.4 Dynamic and Cyclic Loads History Effect on Load-Carrying Capacity of Through-Wall-Cracked Pipe for LBB Fracture Analyses

The objective of this effort was to assess the significance of toughness changes due to cyclic and dynamic load effects on LBB fracture load predictions. These dynamic and cyclic effects have been found to affect the toughness of nuclear piping steels in the IPIRG programs. Deterministic and probabilistic analyses were conducted on this technical aspect.

From the deterministic through-wall-cracked pipe analyses, the results show that the predicted effects of cyclic loading using both the LBB.NRC and GE/EPRI J-estimation schemes are more severe than the existing through-wall-cracked cyclic-loaded pipe test data. Additionally, it was found that the Z-factor approach in SRP 3.6.3 would underpredict the maximum loads if the flaw size effects are taken into account in a detailed EPFM analysis, particularly for pipe diameters larger than 254 mm (10 inches).

The results of the probability analyses show that the conditional failure probability will increase when the dynamic and cyclic loading effects are included via correction factors on the fracture toughness. The magnitude of their resultant effect will, however, depend on the values of correction factors, which in turn are strongly dependent on the quasi-static yield-to-ultimate strength ratio. Based on the results of analyses of some specific pipes, the dynamic and cyclic loading rate can affect the prediction of the conditional failure probability for both austenitic and ferritic pipes. Analyses were not conducted for welds, where there may actually be an improvement in the toughness due to the combined effects of dynamic and cyclic loading.

The probabilistic results are consistent with the deterministic calculations, but the existing experimental data suggest the LBB.NRC and GE/EPRI EPFM analyses overpredict the detrimental effects of cyclic and dynamic loading for through-wall-cracked pipes.

6.1.5 Evaluation of the Effect of Off-Centered Cracks for LBB Evaluations

The objective of this investigation was to assess the effect of having a leaking crack off the center of the bending plane at normal operating conditions, but centered on the bending plane for N+SSE loading. Reference 6.1 gives some prior results. These analyses were conducted in both deterministic and probabilistic evaluations.

The deterministic calculations for the effects of off-centered cracks during leakage of 3.8 liters per minute (1 gpm) on the maximum load-carrying capacity for LBB evaluations showed that there was not a very large difference in the crack lengths between on-centered and off-centered cracks for a given leak rate until the center of the crack was more than 50 degrees off the center of the bending plane.

Further deterministic analyses of the change in the maximum load-carrying capacity relative to the centered leaking crack case showed that the maximum load capacity of the off-centered crack (under normal operating conditions) was less than 10 percent lower than the centered crack maximum load when the middle of the off-centered crack was 50-degrees off the bending plane. This 10 percent difference is considered insignificant.

From the probabilistic analysis, it was found that if the center of the crack is allowed to vary randomly by ± 90 degrees, then the conditional failure probability increased moderately. The change in the conditional probabilities of failure due to the cracks being centered or off centered was roughly equal to the failure probability change of a centered crack experiencing Service Level D stresses rather than Service Level B stresses. The random crack locations with the center being offset more than 50 degrees caused the change in the conditional failure probabilities.

6.1.6 Evaluation of the Effect of Restraint of Pressure Induced Bending on LBB Evaluations

The objective of this evaluation was to assess the effect of the restraint of induced bending from axial stresses in a pipe system. Typical fracture mechanics analyses assume that the ends of a pipe under pressure loading are free to rotate. However, in a pipe system, the attached piping restrains this rotation. This restraint of pipe rotation at the crack plane has two effects. First, it increases the maximum load-carrying capacity. Second, it reduces the crack-opening displacement for leak-rate analyses. These two restraint effects (leak rate versus failure load changes) compete when conducting an LBB analysis.

Both deterministic and probabilistic analyses were conducted to assess the impact of the restraint of pressure-induced bending. In this analysis, it was assumed that a crack was close to a nozzle. The deterministic results showed that the effects on maximum load were insignificant for large-diameter pipe. However, for small-diameter pipe, the effect was tremendous, i.e., the maximum load under the restrained conditions dropped to 11 percent of the unrestrained maximum load. This comes about because: (1) the short crack length in the large diameter pipe made both the restraint effects on the COD and the restraint effect in increasing the maximum loads negligible, and (2) for the small diameter pipe, the effect of restraint on the COD made the leaking crack length very large but the higher load-carrying capacity for the restraint of pressure induced bending loads offers little compensation for the large crack size. This result is significant in regard to application of LBB to smaller diameter pipe.

The probabilistic calculations involved only a large diameter pipe analysis. These results showed that due to the increase in the failure load, the estimated conditional failure probability for a restrained condition was actually slightly lower. This is consistent with the deterministic calculations. Probabilistic calculations for the small diameter pipe case were not conducted.

6.1.7 Evaluation of the Effect of Residual Stresses on Leak-Rate Analyses for LBB

The objective of this analysis was to assess the effect of weld residual stresses on leak-rate analyses for LBB evaluations. Deterministic and probabilistic analyses were conducted on this topic.

Past work during the Degraded Piping Program on as-welded pipe and solution-annealed welds with identical cracks showed that solution annealing to eliminate residual stresses did not improve the load-carrying capacity, and in fact, the solution annealing lowered the fracture loads (probably because the strength of the weld metal was lowered by the solution annealing). Hence, this past work showed that residual stresses in a low toughness stainless steel SAW did not affect the maximum load-carrying capacity. For the present investigation, the effects of residual stresses was only considered for leaking cracks at normal operating stress levels. Results from this program, including work by Brickstad and Moberg of SAQ from an IPIRG-2 round-robin problem (Ref. 6.2), and other concurrent efforts at Battelle (Ref. 6.3) are summarized.

The deterministic results of the effects of weld residual stresses on LBB showed that weld residual stresses can significantly affect the leak rate for pipes welded with tension-compression residual stress fields through the thickness. This is more likely to occur with thinner pipe and hence smaller diameter pipe. The most significant effect on leak rate seemed to be from the effective shortening of the crack length due to the crack-face rotations pinching off the crack opening at the ends of the flaw. An effective countermeasure to this would be to stress-relieve the welds or hydro test to a high enough level to reduce the residual stress fields. Either of these countermeasures would help to mitigate subcritical crack growth as well, but the high-pressure hydro testing would not lower the weld metal strength giving the pipe a higher load-carrying capacity for fracture resistance.

The probabilistic results accounted for residual stresses only by the rotation of the crack faces. The effect of pinching off of the crack opening at the ends of the crack was neglected until the center of the crack was also closed by the residual stress rotation of the crack faces. The results showed significant effects when the crack opening was completely pinched off for the thinner pipe case, but negligible effects of just having the crack faces rotate. Accounting for the crack being pinched off at the ends would change the probabilistic results.

In summary, the residual stress effects were negligible for the thick pipe case, but were highly significant for the thin [less than 25 mm (1 inch) thick] case. Cases between the thin-wall and thick-wall pipes were not addressed in this limited study.

6.2 Uncertainty Analyses Relative to In-Service Flaw Evaluations

6.2.1 Dynamic and Cyclic Loads History Effect on Load-Carrying Capacity of Surface-Cracked Pipe for In-Service Flaw Evaluations

The objective of this effort was to assess the significance of toughness changes due to cyclic and dynamic load effects on surface-cracked pipe predictions for in-service flaw evaluation. Deterministic and statistical analyses were independently conducted on this technical aspect.

The results of the deterministic calculations for TP304 stainless steel base metal were illustrated by calculating the limit load normalized by the EPFM load as a function of the pipe diameter. The EPFM analysis in this case was the SC.TNP1 analysis which has been found to be the most accurate in predicting experimental maximum loads. This normalized load ratio is equivalent to the ASME Z-factor.

The analyses support the conclusion that failure is essentially a limit-load failure for stainless steel base metal pipes of typical size for nuclear plants under monotonic loading. However, for the cyclic/dynamic loading case, failure loads dropped below the limit load. The results showed that the EPFM load-carrying capacity for surface cracks can be reduced by 20 to 30 percent for stainless steel pipes under cyclic dynamic loading compared to quasi-static loading. This calculated effect was validated with experimental results.

The importance of this finding is that an EPFM analysis (i.e., a Z-factor analysis) for stainless steel pipes under N+SSE loading may be needed, but not for normal operating or operating basis earthquake conditions where the cyclic loads are small. The change in load-carrying capacity under cyclic/dynamic loading is significant for N+SSE loads where the ASME applied safety factor is 1.39, and hence a reduction of 20 to 30 percent in load-carrying capacity erodes that margin considerably.

The statistical results were first evaluated by comparing mean loads with and without the effects of cyclic/dynamic loading on the toughness. These results suggest that the effect of cyclic/dynamic loading on the maximum loads was to reduce the maximum loads by about 4 and 10 percent for A516 Grade 70 and TP304 cases, respectively. Interestingly, from the separate deterministic analysis conducted on the TP304 stainless steel pipe with the same pipe diameter, a reduction in maximum loads of about 20 percent was observed. This appears to be an inconsistency between the deterministic and statistical results, so the initial inputs were carefully examined. The deterministic analyses used a stainless steel pipe that had a lower yield-to-ultimate strength ratio than the average value used in the statistical analysis. Hence, the dynamic/cyclic toughness correction was lower in the deterministic analyses than the mean values from the statistical analyses. Thus, the apparent inconsistency can be explained.

To improve such statistical analysis in the future, the yield-to-ultimate strength ratios need to be statistically modeled (with the proper cross-correlation coefficients) and then used to determine a statistical toughness correction due to cyclic and dynamic effects, rather than keeping the toughness correction as a deterministic constant. This would help to better define reasonable bound on dynamic/cyclic toughness corrections for a class of materials.

Results from the deterministic and statistical analyses show that the effect of the yield-to-ultimate strength ratio is an important parameter in determining the corrections to the toughness and the subsequent effects

on the load-carrying capacity. An implication from this is that specifying material with a higher yield-to-ultimate strength would be less susceptible to cyclic degradation. Examples of such materials are low alloy piping steels such as used in German or Japanese PWR piping.

6.2.2 Effect of Uncertainty in UT Flaw Sizing for In-Service Flaw Evaluations

This analysis was conducted to assess the sensitivity of UT flaw sizing variability on the maximum-load capacity of circumferentially surface-cracked pipe. The UT flaw sizing variability data came from 1989 EPRI round-robin results on stainless steel piping. Consistent with the EPRI data, analyses were conducted only on stainless steel pipe, and in particular, they were conducted only for a 28-inch nominal diameter pipe case. This was a statistical analysis where the mean and standard deviations of the UT flaw sizing were varied as a function of flaw depth. Comparisons based on the mean values are described first. The mean value comparison is essentially a deterministic evaluation. Second, comparisons were made involving the standard deviation normalized by the mean value (the coefficient of variance, COV). The COV values are important if a deterministic analysis was to consider using some conservative bound on a mean value to give a higher reliability. Frequently, mean minus one or two standard deviation values are used in bounding deterministic analyses.

The comparison of the mean values of the maximum moments using the UT flaw depth with the moment using the actual flaw depth (deterministic comparisons) showed that the calculated moment accounting for the UT flaw sizing inaccuracy was slightly lower for a/t values of less than 0.35, and then increased for larger a/t values. This trend is consistent with the mean UT flaw depth accuracy trend, i.e., as the flaw depth increases the accuracy gets worse. The magnitude of the moment ratios changes with the flaw a/t , θ/π , and J-estimation scheme used. The SC.TNP1 analysis method, which has been found to give the best agreement with experimental maximum loads, shows that the maximum overprediction of the actual loads range from close to zero percent (for short flaw lengths of any depth) to about 35 percent for the deepest and longest flaw evaluated.

The coefficient of variance (COV) is a measure of the statistical variability of a parameter. If the COV or standard deviation is small, then one might not need to apply a large safety factor to accommodate uncertainty. Conversely, a large COV may make a larger safety factor desirable. Good surface-cracked or through-wall-cracked pipe J-estimation schemes have a COV value around 10 percent for the accuracy of maximum load predictions. The COV values for the UT flaw sizing were found to be 40 to 80 percent, depending on the flaw depth. Shallower flaws had higher COV values than deeper flaws.

A comparison of the COV values from the maximum-load statistical predictions showed that the COV values from the predictions of the maximum loads using the UT flaw sizing variability increase slightly as the flaw depth and lengths became larger. The COV values of the maximum loads due to UT variability were from 11 to 22 percent for the most accurate J-estimation scheme used (the SC.TNP1 analysis). This is a considerably lower COV range than the UT flaw sizing COV of 40 to 80 percent. This reflects some insensitivity in the fracture analyses to flaw depth variability. The larger UT flaw sizing COV was greater for *shallower* a/t values. However, the maximum load COV values were greater for *deep* and long flaws.

This diverse trend of larger COV values for shallow a/t flaw depths from UT (from the EPRI UT round-robin results) versus the maximum load COV values being larger for deep flaws comes from the fact that the maximum loads are more sensitive to deep flaws than shallow flaws. This is especially true for flaws

that are longer. For small flaw lengths, the maximum moments are exceedingly insensitive to crack depth, especially in the Net-Section-Collapse and the SC.TNP1 analyses. Hence, for a short flaw a large variability on flaw depth will result in a small variability on maximum load, and therefore the short flaw maximum loads are more sensitive to material property variations than UT flaw sizing uncertainty.

Results such as these could be used in justifying variable safety margins for UT flaw sizing. For instance, the safety factor need not be as high for shallow flaws even though the COV is highest for them. Similarly, if the flaw length is short, the safety factor could be less than for a long flaw.

6.3 Ranking of Significance of Various Factors Investigated

The significance of the various technical aspects investigated was ranked in importance of how these technical aspects may change results relative to current analysis methodologies. Table 6.1 shows this ranking for both the deterministic and probabilistic results for technical aspects that affect LBB evaluations. Table 6.2 shows this ranking for technical aspects that affect in-service flaw evaluations.

Tables 6.1 and 6.2 also shows a key table number or figure number from the report that was used in establishing the significance of the technical aspect investigated.

These rankings are in the categories of very low, low, medium, high, and very high. An example of a very high ranking is the case of restraint of pressure induced bending on LBB, where for small diameter pipe the difference in the failure loads was a factor of 9 which was much greater than the maximum load safety factor of $\sqrt{2}$ used in LBB analyses. An example of the very low rating is the case of residual stresses on leak-rate analyses for a thick-walled pipe. Here the effects were essentially negligible, except at extremely low operating stresses thought to be lower than practical.

As a final remark, we would like to note that in some cases, it appeared from our initial review that the significance of the deterministic and probabilistic analyses did not agree. Upon further review, however, it was found that most of the deterministic and probabilistic analyses agreed on the significance of a technical aspect. The cases where there were differences in the significance of a technical aspect were found to be due to:

1. The deterministic model being more sophisticated than the model used in the probabilistic analysis. The effect of residual stresses on leak rates was such a case.
2. The material property input not being the same in the deterministic and probabilistic analyses. The effects of cyclic and dynamic loading toughness and load-carrying capacity was such a case.
3. What appeared to be a significant effect over a small range was thought to be less important from a deterministic view, but that same factor gave a major change in conditional failure probabilities. The effect of an off-centered crack on LBB being significant only if the crack was off-centered by more than 50 degrees was such a case.

All of these differences were closely evaluated to make our final ranking.

Table 6.1 Ranking of various technical aspects investigated that affect LBB evaluations

Technical Aspect Investigated	Comments	Significance	Key Table or Figure
Evaluation of different crack morphology default values	For IGSCC	Low	Figure 3.6a
	For corrosion fatigue	Medium	Figure 3.6b
Evaluation of COD dependant and independent crack morphology models for tight crack leak rate analyses	Leak rates > 7.56 l/min (2 gpm)	Low	Figure 3.9a
	3.5 l/min (0.926 gpm) < Leak rate < 4.5 l/min (1.2 gpm)	Medium	Figure 3.9b
	Leak rate < 3.5 l/min (0.926 gpm)	High	Figure 3.9b
Changes of normal operating and N+SSE Stress levels on failure probability	Low normal operating stresses	High	Figures 4.1 - 4.12
	High N+SSE stresses	Medium	Figures 4.1 - 4.12
Dynamic and cyclic loads history effect on load-carrying capacity of through-wall-cracked pipe for LBB fracture analyses	Experimental results show analyses too conservative. (Low cycle fatigue crack growth not evaluated.)	Low	Figure 3.4a
Evaluation of the effect of off-centered cracks for LBB evaluations	Cracks off centered < 50 degrees	Low	Figure 3.11
	Cracks off centered > 50 degrees	Medium	Figure 3.11
	Statistically varying off-center angle	Medium	Figures 4.13 - 4.18
Evaluation of the effect of restraint of pressure induced bending on LBB evaluations	Large diameter	Very low	Table 3.4
	Small diameter	Very High	Table 3.4
Evaluation of the effect of residual stresses on leak-rate analyses for LBB	Thin-walled pipe (i.e., tension to compression stresses through the thickness) at <u>low</u> operating stresses	High to very high	Figure 3.16
	Thin-walled pipe (i.e., tension to compression stresses through the thickness) at <u>high</u> operating stresses	Low	Figure 3.16
	Thick-walled pipe (i.e., tension to compression to tension stresses through the thickness) at typical operating stresses.	Very low	Figure 3.16

Table 6.2 Ranking of various technical aspects investigated that affect in-service flaw evaluations

Technical Aspect Investigated	Comments	Significance	Key Figure
Dynamic and cyclic loads history effect on load-carrying capacity of surface-cracked pipe for in-service flaw evaluations	Low yield-to-ultimate strength materials	Medium	Figure 3.1
	High yield-to-ultimate strength materials	Low	Figures 3.1a - 3.1b
Effect of uncertainty in UT flaw sizing for in-service flaw evaluations	$a/t < 0.25$	Low	Figures 5.6 - 5.7
	$0.25 < a/t < 0.5$	Medium	Figures 5.6 - 5.7
	$a/t > 0.5$	High	Figures 5.6 - 5.7

6.4 References

- 6.1 Rahman, S., Ghadiali, N., Paul, D., and Wilkowski, G., "Probabilistic Pipe Fracture Evaluations for Leak-Rate Detection Applications," NUREG/CR-6004, April 1995.
- 6.2 Rahman, S., Olson, R., Rosenfield, A., and Wilkowski, G., "Summary of Results from the IPIRG-2 Round-Robin Analyses," NUREG/CR-6337, January 1996.
- 6.3 Rahman, S., Dong, P., Wilkowski, G. M., Brickstad, B., Moberg, F., "Effect of Weld Residual Stresses on the Crack-Opening-Area Analysis of Pipes for LBB Applications," *Proceedings of LBB'95 Conference*, Lyon France, October 1995.

APPENDIX A

PROBABILISTIC METHODOLOGY

A.1 Probabilistic Characteristics of a Leakage Size Flaw

Consider a circumferential through-wall-cracked (TWC) pipe shown in Figure A.1 with mean radius, R_m , wall thickness, t , and crack length, $2a$. The pipe is subjected to combined bending and tension stresses under normal operating conditions. The crack length, $2a$, is defined as the LBB detectable flaw size for a given leak rate. Following iterative calculations between thermal-hydraulic and fracture-mechanics analyses, $2a$ can be easily calculated when the leak rate, pipe geometry, material properties, and normal operating loads are specified.

Due to statistical variability of the crack morphology parameters, the flaw size will also be a random variable. Hence, to conduct a probabilistic analysis, the probability distribution of this crack size needs to be specified as well. In this study, a Monte Carlo method was used to determine the probabilistic characteristics of a leakage size flaw. A three-phase approach was adopted. It involved: (1) generation of independent samples of random crack-morphology parameters according to their probability distribution, (2) iterative thermal-hydraulic and fracture-mechanics analyses to determine LBB detectable flaw sizes corresponding to each sample set of crack-morphology parameters and to simulate such samples of flaw size by performing repeated deterministic analyses, and (3) standard statistical analyses of replicated samples of LBB detectable flaw size.

Figure A.1 Schematic representation of pipe with a circumferential through-wall crack subjected to bending and tension

A.2 Structural Reliability Analysis

Let $M_{\max}$ denote the maximum moment-carrying capacity of the pipe (see Figure A.1) with the constant applied tension P (due to constant internal pressure, p). $M_{\max}$ can be obtained from the solution of two nonlinear equations based on J-tearing theory (Ref. A.1). In a generic implicit form, the solution of $M_{\max}$ can be represented by

$$M_{\max} = f(\sigma_y, \sigma_u, F, n, J_{Ic}, C, m, 2a) \quad (A.1)$$

where σ_y is yield stress, σ_u is ultimate stress, F and n are the parameters of a Ramberg-Osgood model for the stress-strain curve, J_{Ic} , C , and m are parameters of a power-law model for J-R curve, and f is a function of various input variables (only the random arguments are shown in the functional dependence of $M_{\max}$). The fracture stability of the leakage size flaw in a pipe can be evaluated by comparing the maximum load-carrying capacity of the pipe (see Equation A.1) with the applied load from N+SSE stresses. Mathematically, the failure criterion can be represented by

$$M_{\max} < M_{N+SSE} \quad (A.2)$$

where M_{N+SSE} is the applied bending moment due to normal plus safe-shutdown earthquake stresses. M_{N+SSE} can be estimated from the knowledge of actual N+SSE stresses in nuclear power plants or from the ASME service levels B, C, or D stress limits defined in Reference A.2. This failure condition can be conveniently expressed in the traditional form: $g(\mathbf{X}) < 0$, where the performance function,

$$g(\mathbf{X}) = M_{\max} - M_{N+SSE} = f(\sigma_y, \sigma_u, F, n, J_{Ic}, C, m, 2a) - M_{N+SSE} \quad (A.3)$$

in which $\mathbf{X} = \{\sigma_y, \sigma_u, F, n, J_{Ic}, C, m, 2a, M_{N+SSE}\}$ is an augmented vector of input random parameters characterizing uncertainty in all load and system parameters. Note that the performance function, $g(\mathbf{X})$, itself is random, because it depends on the input vector, $\mathbf{X}$, which is random. The reliability, P_s , is the complement of the conditional probability of failure, P_F (CPOF), i.e., $P_s = 1 - P_F$. P_F is defined as the probability that the failure event represented by $g(\mathbf{X}) < 0$, i.e.,

$$P_F \stackrel{\text{def}}{=} \Pr [g(\mathbf{X}) < 0] \stackrel{\text{def}}{=} \int_{g(\mathbf{x}) < 0} f_{\mathbf{X}}(\mathbf{x}) d\mathbf{x} \quad (A.4)$$

where $f_{\mathbf{X}}(\mathbf{x})$ is the joint probability density function of the random vector, $\mathbf{X}$, which is assumed to be known. When $\mathbf{X}$ follows a normal or lognormal distribution, $f_{\mathbf{X}}(\mathbf{x})$ can be obtained from the marginal density function for each component of $\mathbf{X}$ and their correlation characteristics. The distribution parameters along with the correlation properties of $\mathbf{X}$ were determined from standard statistical analyses of experimental data. See Reference A.1 for further details on the procurement of data and their statistical characterization.

In general, the multi-dimensional integral in Equation A.4 cannot be determined analytically. As an alternative, numerical integration can be performed; however, it becomes impractical and the computational effort becomes prohibitive when the dimension of $\mathbf{X}$ becomes greater than two and, in this case, one may have a maximum of nine dimensions according to Equation A.3.

Several approximate methods exist for performing the multi-dimensional probability integration in Equation A.1. Among them, First- and Second-Order Reliability Methods (FORM/SORM), Importance Sampling, Directional Simulation, Monte Carlo Simulation, and others can be applied to estimate P_F in Equation A.1. For details of these methods, see Reference A.1 and other references cited therein. In this study, SORM (Ref. A.3) was used to compute the probability failure. In Reference A.1, the probability estimates by SORM were validated against the results of Monte Carlo and Importance Sampling analyses. It was also found in that reference that SORM can calculate the failure probability of pipes with much less computational effort than that required by the Monte Carlo method. See Reference A.1 for further details on the accuracy and computational efficiency of SORM.

Note that P_F is defined here as a conditional probability of failure. The principal conditions are that (1) the pipe is leaking with an LBB detectable flaw size and (2) an earthquake occurs with $N+SSE$ stresses, resulting in an applied bending moment equal to M_{N+SSE} , also during leakage.

A.3 The Computer Codes PSQUIRT and PROLBB

PSQUIRT is a computer code that was developed during the Short Cracks in Piping and Piping Welds Program to estimate the probability density function of an LBB detectable flaw size. It is based on direct Monte Carlo simulation. PSQUIRT, which stands for Probabilistic Seepage Quantification of Upsets in Reactor Tubes, is a combination of three independent programs for conducting pre-processing of input, thermal-hydraulic and fracture-mechanics analyses, and post-processing of output. For further details, see References A.1 and A.4.

PROLBB, which is an acronym for **PRO**babilitistic **Leak-Before-Break**, was also developed during the Short Cracks in Piping and Piping Welds Program to evaluate failure probability of flawed nuclear piping subjected to combined stresses due to tension and bending. The deterministic part of PROLBB is based on the LBB.ENG2 method (Ref. A.1). The fracture-mechanics equations for this method, used to calculate the J-integral and its applications for computing maximum loads, were defined in Reference A.1. The probabilistic part of PROLBB is based on (1) First-Order Reliability Method, (2) Second-Order Reliability Method, (3) Importance Sampling, and (4) Monte Carlo Simulation. Further details of PROLBB code can be obtained from Reference A.1.

A.4 References

- A.1 Rahman, S., Ghadiali, N., Paul, D., and Wilkowski, G., "Probabilistic Pipe Fracture Evaluations for Leak-Rate-Detection Applications," NUREG/CR-6004, U.S. Nuclear Regulatory Commission, Washington, D.C., April 1995.
- A.2 1989 Addenda ASME Boiler & Pressure Vessel Code - Section III, Article NB-3652.

- A.3 Madsen, H. O., Krenk, S., and Lind, N. C., *Methods of Structural Safety*, Prentice-Hall, Inc., Englewood Cliffs, New Jersey, 1986.
 - A.4 Rahman, S., Ghadiali, N., Wilkowski, and Paul, D., "A Computer Model for Probabilistic Leak-Rate Analysis of Nuclear Piping and Piping Welds," Proceedings of *ASME/JSME 1995 Pressure Vessel and Piping Conference*, Honolulu, Hawaii, June 1995.
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