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Evaluation and Refinement of Leak-Rate Estimation Models

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ABSTRACT

Leak-rate estimation models are important elements in developing a leak-before-break methodology in piping integrity and safety analyses. Existing thermal-hydraulic and crack-opening-area models used in current leak-rate estimations have been incorporated into a single computer code for leak-rate estimation. The code is called SQUIRT, which stands for Seepage Quantification of Upsets In Reactor Tubes. The SQUIRT program has been validated by comparing its thermal-hydraulic predictions with the limited experimental data that have been published on two-phase flow through slits and cracks, and by comparing its crack-opening-area predictions with data from the Degraded Piping Program. In addition, leak-rate experiments were conducted to obtain validation data for a circumferential fatigue crack in a carbon steel pipe girth weld.

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ABBREVIATIONS AND SYMBOLS

Abbreviations

ACO	area of crack opening
ACO _e	elastic area of crack opening
ASME	American Society of Mechanical Engineers
BWR	boiling water reactor
CFR	code of federal regulation (in USA)
COD	crack-opening displacement
DP ³ II	Degraded Piping Program--Phase II
EDM	electric discharge machining
EPFM	elastic-plastic fracture mechanics
EPRI/GE	Electric Power Research Institute/General Electric
FEM	finite element method
IPIRG	International Piping Integrity Research Group
J-R	J-resistance
LBB	leak-before-break
LEFM	linear-elastic fracture mechanics
LRE	leak-rate estimation
NRC	U.S. Nuclear Regulatory Commission
PICEP	Pipe Crack Evaluation Program (EPRI)
PWR	pressurized water reactor
SAW	submerged arc weld
SQUIRT	Seepage Quantification of Upsets in Reactor Tubes
TH	thermal-hydraulic

Symbols

a	half crack length
a _e	effective half crack length
A _c	cross-sectional flow area at crack exit plane
A _i	cross-sectional flow area at plane where L/D _i = 12
A ₀	cross-sectional flow area at crack entrance plane
b	half of pipe circumference
B	constant in Equation (4.3)
C ₁ , C ₂	constants in the friction factor correlation

C_D	coefficient of discharge
D_i	inside diameter of pipe
D_H	hydraulic diameter
e	number of velocity heats per unit flow path length
e_v	total loss coefficient over flow path
E	elastic modulus
f	friction factor
f_2	LEFM crack-opening displacement function in EPRI/GE method
$\bar{G}$	average mass flux of fluid
G_c	mass flux of fluid at crack exit plane
G_o	mass flux of fluid at crack entrance plane
G_s	mass flux in single-phase region of crack flow
G_T	mass flux in two-phase region of crack flow
$\bar{G}_T$	average mass flux in two-phase region of crack flow
h_a	incremental correction factor for next guess at the mass flux term
h_2	full plastic crack-opening displacement function in EPRI/GE method
h_b	incremental correction factor for next guess at the exit pressure term
H_o	enthalpy of fluid at crack plane entrance
I_b	nondimensional LEFM crack-opening function for a pipe in bending
I_t	nondimensional LEFM crack-opening function for a pipe in tension
J	J-integral fracture parameter
k	surface roughness
K	stress-intensity factor
L	flow-path length
M	moment
M_o	limit moment at reference stress
n	strain-hardening coefficient
N	variable defined by Equation (4.5)
p	internal pressure
p_a	pressure loss due to acceleration
p_{aa}	pressure loss due to area change acceleration
p_c	absolute pressure at exit plane of crack
p_e	pressure loss due to entrance effects
p_f	pressure loss due to friction

p_k	pressure loss due to protrusions in the crack path
p_o	absolute pressure at entrance plane at crack
P	load on a pipe
P_o	axial limit load at reference stress
R	mean radius
S	applied stress/yield strength
S_{gc}	entropy of saturated vapor at crack exit plane pressure
S_{Lc}	entropy of saturated liquid at crack exit plane pressure
S_o	entropy of the liquid at the crack entrance plane pressures
T_o	temperature at crack plane entrance
W	mass flow rate through the crack
$\bar{X}$	average fluid quality
X_c	fluid quality using nonequilibrium vapor generation rate in Eq. 4.1
X_E	equilibrium fluid quality
X_o	fluid quality at crack plane entrance
X_t	critical quality
α	coefficient in Ramberg-Osgood relation
δ	crack-opening displacement
δ_e	elastic component of displacement
δ_p	plastic component of displacement
ϵ	strain
ϵ_o	reference strain in Ramberg-Osgood relation
ϵ_p	plastic strain
γ	plastic-zone correction on ACO
γ_o	the isentropic expansion coefficient
q	Henry's mass flux equation
ψ_o	value of Eq. 4.1 at most recent guess of G_c and P_c
ϕ	pipe rotation
ϕ_e	elastic pipe rotation
ϕ_p	plastic pipe rotation
v_{gc}	specific volume of saturated vapor at exit pressure
$\bar{v}_g$	average volume of saturated vapor at average crack pressure
v_{Lc}	specific volume of saturated liquid at exit pressures
v_{Lo}	specific volume of saturated liquid at entrance pressure

$\bar{v}_L$	average volume of saturated liquid at average crack pressure
θ	half crack angle
θ_e	effective half crack angle
σ	stress
σ_b	bending stress
σ_0	reference stress in Ramberg-Osgood relation
σ_t	axial tension stress
σ_y	yield strength
Ω	pressure balance equation defined by Eq. 4.2
Ω_0	value of Eq. 4.2 at most recent guess of G_C and P_C

EXECUTIVE SUMMARY

The evaluation and refinement of leak-rate estimation models were conducted in three distinct stages. The first stage was a review of existing thermal-hydraulic models and crack-opening-area models. The second stage was the development of an appropriate computer model for leak-rate estimation. The code is called SQUIRT, which stands for Seepage Quantification of Upsets In Reactor Tubes. In the third stage, SQUIRT model predictions were compared with published experimental data and data from limited validation experiments that were conducted using a pipe specimen with a fatigue crack in a girth weld. The development of the SQUIRT code enables licensing authorities and industry users to conduct the leak-rate evaluations for leak-before-break applications in a more efficient manner. The SQUIRT code also includes technical advances that are not available in other computer codes currently used for leak-rate estimation.

The review of existing thermal-hydraulic models indicated that the Henry-Fauske model was the best currently available representation of fluid flow through tight cracks in a piping system. This model allows for nonequilibrium vapor generation rates as the fluid flows through the crack. The rate at which vapor is formed approaches the equilibrium value using an exponential relaxation correlation, with the correlation coefficients determined from the experimental data of Henry. In addition to the uncertainty associated with specifying the nonequilibrium vapor generation rate, other uncertainties in the analysis arise due to incomplete knowledge of the flow path losses, the friction factors for tight cracks, and the potential for particulate plugging. The impact of each of these uncertainties on the thermal-hydraulic analysis is discussed in this report.

The review of the area of crack opening models indicated that the EPRI/GE estimation method, combined with the assumption that the crack-opening profile is elliptical, consistently provides accurate estimates for cracks in unwelded pipes. For cracks in welds, the estimates of the area of crack opening obtained using existing models were found to be accurate in the linear-elastic fracture mechanics (LEFM) range, but generally inaccurate in the elastic-plastic fracture mechanics (EPFM) range. The above conclusions were reached by comparing the results of estimation models with experimental data, and with the results of elastic-plastic finite element analysis from the Degraded Piping Program--Phase II.

The Henry-Fauske model of thermal-hydraulic behavior and the EPRI/GE EPFM model for determining the area of crack opening have been incorporated into the SQUIRT code. The SQUIRT code can be used to predict the area of crack opening and leakage flow rate from a cracked pipe section under a given load. It represents an advance over the EPRI PICEP code and offers three advantages. First, the SQUIRT code accounts for crack growth at higher loads. Second, the maximum load in SQUIRT is calculated by elastic-plastic fracture mechanics, in contrast to PICEP, which uses limit-load analysis to calculate the maximum load. Finally, although both PICEP and SQUIRT can calculate the leakage rate for known loads, crack sizes, thermal-hydraulic conditions, and other pressure losses, only the SQUIRT code can directly calculate the crack size for a given load, leakage rate, thermal-hydraulic condition, and pressure loss coefficient.

The SQUIRT thermal-hydraulic model predictions were compared to experimental data on two-phase flow through long tubes, two-phase flow through slits, and two-phase flow through actual cracked pipe. Additional experimental validation data were provided by leak-rate tests performed using a circumferential fatigue crack in a carbon steel girth weld. The cracked pipe specimen was mounted in a four-point bending apparatus and the leakage flow rate and crack-opening displacement were measured as a function of the applied bending load and internal pressure. The applied bending loads were sufficiently low that the cracked pipe behaved in a predominantly elastic manner. Fluid pressure and temperatures were varied to simulate both BWR and PWR thermodynamic conditions. To obtain reasonably good agreement between the experimental results and the SQUIRT predictions for the area of crack-opening and leakage flow rates, a flow path loss coefficient of approximately 6 velocity heads per mm of crack flow path length had to be assumed. This implies that the tight fatigue crack in the girth weld causes the fluid to change direction many times while flowing through the crack.

As a recommendation for further work, a detailed contour mapping of the crack faces should be performed to verify the validity of this assumption for the flow path loss coefficient. The sensitivity of this parameter should be evaluated also, and guidelines given to users.

Based on experimental data as well as results of elastic-plastic finite element analysis, it appears that the crack-opening profile of circumferential cracks in pipes under bending can be approximated reasonably well by an ellipse. The elliptical profile, combined with crack-opening displacement values obtained by the EPRI/GE estimation method, provide acceptably accurate estimates of crack-opening area in unwelded pipes with cracks. For pipes with cracks in welds, additional work is needed to obtain consistently accurate crack-opening displacement estimates by the EPRI/GE method at high loads. At low loads, when the deformations in the pipe are predominantly elastic, the method provides reasonably accurate results even in welded pipes. Additional work is needed also to quantify the difference between crack-opening areas on the inside and the outside pipe surfaces for relatively thick-walled pipes.

1.0 INTRODUCTION

Development of leak-rate estimation methodology was initiated in response to intergranular stress corrosion cracking in boiling water reactor (BWR) piping. Further interest in this area was stimulated by investigations into the application of a leak-before-break (LBB) philosophy to piping integrity safety analyses instead of assuming a double-ended guillotine break. Adoption of an LBB philosophy requires reliable leak detection systems and verified leak-rate estimation techniques. Accurate leak-rate prediction requires correlation of crack size and shape, and is necessary to evaluate the ability of normal makeup systems to handle potential leakage. The development of a verifiable leakage rate assessment methodology is critical to LBB evaluations. Regulatory implications include the elimination of pipe whip restraints and jet impingement shields, as well as changing requirements for equipment qualification for the case of steam released from a break.

1.1 Approach

This task was initiated with an analytical effort designed to provide an integrated leak-rate estimation (LRE) method. Data required to assess this method were identified. For the area of crack-opening structural analysis, existing crack-opening data from the Degraded Piping Program were used to check the various models. For the thermal-hydraulic analyses, existing experimental data were used to verify a leakage model based on the Henry-Fauske two-phase critical flow formulation (Refs. 1 and 2). Cracked pipe leak-rate experiments were then conducted on a pipe specimen containing a fatigue crack in a girth weld. These additional experimental data were used to provide further verification of the crack-opening area and thermal-hydraulic models.

Previous work by Collier et al. (Ref. 3) suggested that the leakage rate from extremely tight cracks may be affected by particulates in the flow system. An additional goal of this study has been to determine the size distribution and concentration of particulates likely to exist in nuclear power plant piping systems, and to assess the potential for these particulates to cause partial plugging of pipe cracks.

The remainder of this report is organized as follows: Section 2 contains a review of existing thermal-hydraulic (TH) models used to predict the leakage flow through cracked pipe. Particular emphasis is placed on the limitations of these models and the uncertainties associated with the analyses of actual cracked pipe. Section 3 reviews existing area of crack opening (ACO) models* and compares the models to existing data in order to establish the requirements for an appropriate ACO model. Section 4 describes the SQUIRT (Seepage Quantification of Upsets In Reactor Tubes) computer program which contains appropriate TH and ACO models as determined from the above-mentioned

* Previously the term COA was used for crack-opening area, but this created confusion with the term COA referring to crack-opening angle in fracture mechanics. Consequently, the term ACO, area of crack opening, was created to circumvent possible confusion.

investigations. The detailed mathematical formulation of the models and comparisons with existing experiment data are included in the section. Section 5 describes the leak-rate experiments conducted to provide further verification of the TH and ACO models. This section contains a description of the facility, the pipe test specimen, the test procedures, the ACO measurements, and the TH measurements. Section 6 summarizes the important findings of this study and includes recommendations for future work. Appendix A contains a listing of the leak-rate experimental data used to verify the SQUIRT computer program TH model.

2.0 REVIEW OF THERMAL-HYDRAULIC MODELS

The two-phase critical flow of water through piping systems is a highly complex physical phenomenon that has been widely studied for the last 40 years. What makes this problem so difficult is the existence of the two phases in the flow system which can interact in a variety of ways. For instance, a two-phase flow system can exist with either vapor bubbles dispersed in a continuous liquid phase or as liquid droplets dispersed in a continuous vapor phase. The physics of each of these situations is vastly different, yet each represents a two-phase flow.

Simple models of two-phase flow systems (Ref. 4) assume the fluid to be a homogeneous mixture of the gas and liquid phases. Mass, momentum, and energy balances are then written for the homogeneous mixture. The equations are solved for a single fluid having the average properties of the mixture at any point in the system. Although this is a reasonable first approach to attempt when modeling two-phase systems, significant errors in the predicted system mass flow rates can occur for systems in which nonequilibrium interactions are taking place, or in systems where large differences in the velocities of the two phases exist. These errors can be as large as a factor of two or more when comparing the models and experiments (Isbin, Ref. 5).

To overcome some of the limitations of the homogeneous equilibrium model, many authors have suggested refinements to the model to make it more realistic. This led to the development of the slip flow models of Zivi (Ref. 6), Fauske (Ref. 2), and Moody (Ref. 7). In these models the gas is assumed to have a higher velocity than the liquid. The ratio of gas velocity to liquid velocity is referred to as the slip ratio. By incorporating the slip ratio in the homogeneous equilibrium model equations, good agreement can be obtained between the model and experiment in a number of instances.

However, further complications arise when the two-phase mixture is experiencing critical flow. In this case, the time required for the fluid to reach thermodynamic equilibrium when moving into regions of lower pressure is comparable to the time that the fluid is flowing in the crack. This leads to nonequilibrium vapor generation rates for two-phase critical flows. To account for nonequilibrium effects between the phases, Henry (Ref. 1) and Fauske (Ref. 2) have proposed a simple model for the nonequilibrium vapor generation rate. In this model they assume that the mixture quality relaxes in an exponential manner toward the equilibrium quality that would be obtained in a long tube. The relaxation coefficient was calculated based on their experiments with the critical flow of a two-phase water mixture in long tubes ($L/D_i > 100$). The Henry-Fauske model is the one that has been chosen in this study to model the two-phase critical flow of water through cracks in piping systems. It is also the basis of the PICEP (Pipe Crack Evaluation Program) computer program developed by the Electric Power Research Institute (EPRI) (Refs. 8 and 9).

In the mid-seventies, a number of researchers [Bouré (Ref. 10), Ishii (Ref. 11), and others] proposed a two-fluid model of two-phase flow. In a two-fluid model, the gas and liquid phases are each modeled separately, but interaction terms are included in the conservation equations to represent the

interphase transport of mass, momentum, and energy. Although these multifluid models are more detailed than the single fluid models, not enough information is currently known about the mechanisms of interphase transport to accurately determine the correct expressions to use for the interactive terms [Drew (Ref. 12) and Ramshaw and Trapp (Ref. 13)]. For this reason, all multifluid models contain numerous unknown coefficients which are usually supplied via estimation or by adjusting the coefficients until good agreement is obtained with available experimental data. However, progress is being made in this area and, eventually, multifluid models may replace less detailed single fluid models. But for the time being, the accuracy of the single fluid models is adequate and their relative ease of solution makes them attractive for modeling purposes.

2.1 Uncertainties in the Analysis

In all of the leak-rate models, uncertainties arise in the analysis due to incomplete knowledge of the crack geometry and the physics of the flow phenomena.

2.1.1 Crack Geometry

For the purposes of modeling, a pipe crack is usually assumed to have the shape of either a rectangle, an ellipse, or a diamond. For the purposes of illustration, Figure 2.1 shows a rectangular, elliptic, and diamond-shaped crack, each with aspect ratios ($2a/\delta$) of 4. Typical fatigue or stress corrosion cracks in piping systems have aspect ratios of several hundreds (or thousands).

The cross section flow area for leak-rate analyses is strongly dependent on the shape of the crack assumed in the modeling process. If the area of each crack shown in Figure 2.1 is normalized to that of the rectangular crack, then the flow areas are 1.000, 0.785, and 0.500 for the rectangular, elliptic, and diamond-shaped cracks, respectively. Thus, a difference in flow rates of about a factor of two is possible based solely upon whether the crack shape is assumed to be either rectangular or diamond-shaped.

For all of the analyses in this report, the crack shape was assumed to be elliptical. Elliptical-shaped cracks have flow areas that are about halfway between the extremes of the rectangular and diamond-shaped cracks. This "middle-of-the-road" approach is probably adequate for fatigue cracks, which tend to lie primarily in straight lines, but is probably much too simple an approximation for stress corrosion cracks, which tend to meander through a pipe specimen. The accuracy of the leakage flow calculations could be greatly improved by having an accurate representation of the actual crack flow area for the different types of cracks (fatigue, stress corrosion, etc.). This is one area where further research to better define the crack geometry would be helpful for more accurate flow modeling.

Figure 2.1 The commonly assumed crack-opening shapes: (a) rectangular, (b) diamond, and (c) elliptical.

2.1.2 Flow Path Losses

In addition to crack flow area, the surface roughness and number of bends on the crack faces are also primary factors in determining leakage flow rates. Figure 2.2 illustrates a crack leakage flow path with numerous bends and significant surface roughness on each of the crack faces.

A significant pressure loss occurs every time the fluid must change direction along the flow path. In fatigue and stress-corrosion cracks, the number and severity of the bends can in some circumstances account for upwards of one-half the total pressure loss of the fluid when flowing through the crack. Norris et al. (Ref. 8) have shown this parameter to be of importance for stress-corrosion cracks. In the past, this parameter was thought to be of lesser importance for fatigue cracks because fatigue cracks generally break through in a fairly flat plane. However, the experimental results shown later in this report indicate that the number of bends in the flow path can be significant even for fatigue cracks. This occurs because the variations in the contours of the relatively flat plane of a fatigue crack are large compared to the crack-opening displacement (COD). Therefore, even though the faces of a fatigue crack appear to be fairly flat to the naked eye, they contain many flow path bends because of the tightness of the crack.

2.1.3 Friction Factors

Another uncertainty in the analysis is associated with the choice of a proper friction factor for flow in a tight crack. The friction factor relationship most often used in leak-rate analyses is the modified form of the Von Karman relation (Ref. 14, p. 584):

$$f = [C_1 \log(D_H/(2k)) + C_2]^{-2} = [C_1 \log(D_H/k) + C_2 - C_1 \log(2)] \quad (2.1)$$

where k is the surface roughness and D_H is the hydraulic diameter. For flow in circular tubes, $C_1 = 2.0$, $C_2 = 1.74$, and D_H is equal to the pipe internal diameter D_i . For noncircular tubes, the same expression is used, except that the hydraulic diameter is defined by

$$D_H = \frac{4(\text{Area})}{\text{Wetted Perimeter}} \quad (2.2)$$

Although the above expression has been verified for tubes of rectangular, triangular, and trapezoidal cross sections by Schiller (Ref. 15) and Nikuradse (Ref. 16), the shapes that were tested were not grossly skewed from a circular cross section. For the case of flow through tight cracks, the cross-sectional shape is grossly different from that of a circle, and it is not apparent that the hydraulic diameter method is valid. For example, Brighten and Jones (Ref. 17) provide data on flow through an annulus, which is similar to flow between two parallel plates. They have shown that the friction factors for annular flows are higher than those predicted by the hydraulic diameter method. Unfortunately, Brighten and Jones did not have enough data on small annular gap thicknesses to be able to derive an equivalent expression for the friction factor for parallel plate flows.

Figure 2.2 Two primary factors affecting leakage flow rate are the number of bends and the surface roughness of each crack face.

More recently, data on gas flow through narrow slits, collected by Button et al. (Ref. 18), suggest values of $C_1 = 2.25$ and $C_2 = 1.25$ for the coefficients in the friction factor correlation, Eq. (2.1). Similarly, recent data by John et al. (Ref. 19) suggest that values of $C_1 = 3.39$ and $C_2 = 0.866$ be used for narrow slits when the parameter D_H/k is less than about 100. Using either of the above correlations for narrow slits will, in general, give much higher values for the friction factor than the modified Von Karman relation.

2.1.4 Nonequilibrium Vapor Generation Rates

The Henry (Ref. 1) model assumes nonequilibrium vapor generation rates for the critical flow of a two-phase mixture of water. This is a reasonable assumption because the residence time of the water in the system is comparable to the time required for the water to be converted to vapor. Based on observations obtained in the laboratory, Henry (Ref. 20) assumed that the mixture quality in a short tube ($L/D_i < 100$) would approach the equilibrium mixture quality in about 100 pipe diameters. Furthermore, he observed that no vapor was formed until the liquid reached about 12 pipe diameters from the entrance.

Since the time of Henry's experiments, some additional experiments have been run by Swamy (Ref. 21) and by French researchers (Ref. 22) that apparently suggest that the start of vapor generation might take place much farther downstream of the pipe entrance in certain circumstances. There are many parameters which affect where the vapor will begin to form. Because of the unstable nature of the phenomenon, the starting point of vapor formation fluctuates significantly even during the course of an experiment. However, these additional experimental data have never been published and, therefore, are not available for open scrutiny. The initiation and rate of vapor generation in two-phase critical flow remain as important uncertainties in the analysis.

2.1.5 Particulate Plugging

For cracks that are very tight, i.e., those with CODs of the same order of magnitude as the particulate size, particulates may lodge in the crack. This partial plugging of the pipe crack by particulates could reduce the leakage rate through the crack and thereby affect LBB analyses. In order to gauge the potential for particulate matter to partially plug a crack, a literature survey was conducted to determine the size, range and concentrations of particulates in pressurized water reactors (PWRs) and BWRs during normal operation.

Table 2.1 summarizes the results of this literature survey (Refs. 23 through 28). Under normal operating conditions, the principal composition of particulates is nickel ferrite in PWRs and iron oxide magnetite and hematite in BWRs. In PWRs and BWRs, the mass median diameter of the particulates falls in the range of 0.5 to 5 μm with a very broad range of particle sizes present (from 0.01 to 100 μm). In the primary water, the particulate concentration is held below 1 ppm. In the makeup water, the concentration is less than 0.5 ppm.

Table 2.1 Particulates in primary system water.

Water Chemistry	PWRs	BWRs
Principal Particulate Composition	Nickel Ferrite	Iron Oxide Magnetite and Hematite
Primary Water Particulates		
Size Range, μm	0.01-100	0.01-100
Mass Median, μm	0.5-5	0.5-5
Concentration, ppm	<1.0	<1.0
Makeup Water Particulates		
Concentration, ppm	<0.5	<0.5

Are these particles large enough to plug a tight crack in a piping system? The answer is a qualified yes. Although the size of certain types of cracks in piping systems, such as intergranular stress corrosion cracks, can be on the same order of magnitude as the particle sizes, the concentrations of the particulates are low enough that particulate plugging would occur only for cracks that had been leaking for days or months at a time. But this situation could certainly occur at a plant where visual inspections are very infrequent and difficult at best.

The above literature survey was limited to particulate sizes found under normal operating circumstances. During an upset to the primary system, such as occurred at the Duane Arnold plant in 1978 when some resin was accidentally released into the primary system, it is possible to get an abnormal size and concentration of particulates in the primary water. These situations need to be examined on a case-by-case basis and were not considered in this survey.

3.0 CRACK-OPENING-AREA MODELS

3.1 Requirements of an Appropriate Area of Crack Opening (ACO) Model

Generally, leak-rate calculations are performed for one of the following two purposes:

- (1) Given a flaw size, pipe dimensions, material stress-strain properties, and loading, it is desired to know the fluid leak rate through the crack. The aim is to estimate whether the given flaw size would result in a reliably detectable leak rate. Therefore, the aim is to underestimate rather than overestimate the ACO.
- (2) Given a leak rate, it is desired to know what the ACO must be. Then, knowing the ACO, pipe dimensions, material properties, and loading, the aim is to estimate the flaw size, which is subsequently used to determine the pipe's load-carrying capacity. For this purpose, it is more desirable to have an overestimate than an underestimate of ACO.

For either of the two purposes, it is desirable to have a mathematical model which is sufficiently accurate and relatively simple and inexpensive to use. For example, a detailed finite element analysis model, while generally accurate, would have very limited use because it would be too expensive and time-consuming to be used routinely. What is needed is a relatively simple equation (or a set of equations) to estimate flaw sizes and ACOs.

3.2 Review of Currently Used Models

Simple mathematical models, often referred to as estimation models, are almost invariably based on assumptions which are necessary to minimize the need for elaborate numerical techniques. Typically, such assumptions lead to simpler representation of the material's stress-strain behavior, flaw shape and orientation, loading condition, etc. The available estimation models can be broadly classified as follows.

- (1) Linear-elastic fracture mechanics (LEFM) models
- (2) Pseudoplastic fracture mechanics models
- (3) Elastic-plastic fracture mechanics (EPFM) models.

In the following, we present a brief review of these approaches.

3.2.1 Linear-Elastic Fracture Mechanics (LEFM) Models

The LEFM models are based on the assumption that even at the maximum applied loads of interest, the material behavior everywhere in a pipe containing a through-wall crack remains linear-elastic. A number of linear-elastic analysis solutions for through-wall cracks in pipes are available in the literature. For examples, see Erdogan (Ref. 29), Erdogan and Delale (Ref. 30), and Sanders (Refs. 31 and 32). Many of the elastic solutions in the literature appear in

terms of the stress intensity factor (K) rather than the COD or ACO. However, the available K solutions can be utilized to determine COD and ACO using the Castigliano's theorem, as described in Ref. 33 or, alternatively, in the work of Wuthrich (Ref. 34).

The neglect of crack-tip plasticity in LEFM models leads to underestimates of ACO for a given load acting on the pipe. For a prescribed remote displacement or rotation, an LEFM model will tend to overestimate the ACO. In many practical situations, where the flaw size is small compared to the pipe circumference and where the loads are low, the error (positive or negative) caused by neglecting crack-tip plasticity will be insignificantly small.

3.2.2 Pseudoplastic Fracture Mechanics Models

The pseudoplastic fracture mechanics models for calculating ACO are based on simplified representations of crack-tip plastic deformation. Generally, the reason for these simplified representations is to circumvent the solution of nonlinear equations which are posed in elastic-plastic problems. Instead, only a linear-elastic stress analysis problem is solved.

Two extreme cases of crack-tip plastic zone size are amenable to pseudoplastic modeling. These are small-scale yielding and net-section yielding. Under small-scale yielding, in which the plastic zone is assumed to be small compared to all the other dimensions of the cracked body, Irwin (Ref. 35) or Dugdale (Ref. 36) plastic-zone corrections can be effectively utilized to estimate ACOs. This is the approach followed by Paris and Tada (Ref. 37), and later adopted by Klecker et al. (Ref. 38). For a through-wall circumferential crack in a pipe (see Figure 3.1) subjected to axial tension and remote bending, Paris and Tada give the expression for ACO as:

$$ACO = \frac{\pi R^2}{E} [\sigma_t I_t(\theta_e) + \sigma_b I_b(\theta_e)] \quad (3.1)$$

where

R is the mean pipe radius,

E is Young's modulus,

σ_t is the axial stress,

σ_b is the maximum bending stress, and

I_t and I_b are nondimensional functions of the half-crack angle, θ .

The nondimensional functions are derived using linear-elastic analyses (see Refs. 37 and 38). To account for small-scale yielding, I_t and I_b are evaluated using the effective half crack angle (θ_e) rather than the actual half crack-angle (θ).

Figure 3.1 Schematic representation of pipe with a circumferential through-wall crack subjected to axial tension and bending.

The effective half crack angle is defined using the Irwin plane-stress plastic zone correction as

$$\theta_e = \theta + \frac{K^2}{2\pi R \sigma_y^2} \quad , \quad (3.2)$$

where σ_y is the yield strength of the material and K is the linear-elastic mode I stress intensity factor due to combined tension and bending loads acting on the pipe.

Wuthrich (Ref. 34) has given an expression for ACO for through-wall axial and circumferential cracks in pipes subjected to membrane forces. The results are presented as the product of a plasticity correction factor (γ) and the area of crack opening, $(ACO)_e$, calculated by linear-elastic analysis, that is,

$$ACO = \gamma (ACO)_e \quad . \quad (3.3)$$

Wuthrich gives γ based on both the Irwin (Ref. 35) and Dugdale (Ref. 36) plastic-zone corrections. A comparison of the two approaches is given in Figure 3.2, which shows γ as a function of applied stress to yield strength ratio (S). It is seen that for small values of S (small-scale yielding) the two approaches give approximately the same result. For larger values of S , the plasticity correction factors associated with the two approaches deviate from each other. But for larger S values the small-scale yielding condition is violated, and neither approach should be expected to be accurate. Instead, it may be more appropriate to use another pseudoplastic model based on the assumption that the uncracked ligament is fully yielded. One such model, proposed by Smith (Ref. 39), gives the following expression for ACO for a circumferential through-wall crack in a pipe under pure bending:

$$ACO = 4R^2 \phi_p \left[1 - \cos \frac{\theta}{2} + \frac{1}{2} \sin \theta \right] \quad (3.4)$$

where ϕ_p is the plastic component of the pipe rotation (ϕ).

While both the small-scale yielding and the limit-load type pseudoplastic models are useful in specific situations, they cannot be expected to provide accurate ACO estimates over the entire range of load magnitudes, flaw sizes, and material types of interest in nuclear power plant piping applications. The main reason is that a large number of situations are such that the crack-tip plastic deformation can be characterized neither as small-scale yielding, nor as net-section yielding. Rather, the analysis problem is elastic-plastic, requiring nonlinear analysis methods.

Figure 3.2 Plasticity correction factors using the Irwin (Ref. 35) and Dugdale (Ref. 36) models.

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3.2.3 Elastic-Plastic Fracture Mechanics (EPFM) Models

In general, ACO estimates in the elastic-plastic regime are possible only by numerical analysis techniques, such as the finite element method (FEM). For a pipe containing a through-wall crack, either a three dimensional or a shell formulation must be used to compute the ACO. Such computations are too time-consuming and expensive to be used for routine LBB assessments. On the other hand, general closed-form solutions, even with simplified representations of the material's nonlinear stress-strain behavior, are difficult to develop.

Kumar, German, and Shih, in pioneering work sponsored by EPRI (Ref. 40), developed a method which enables one to generalize selected FEM solutions to be applicable to a wide range of flaw and pipe sizes and materials. This generalization is possible because of the key assumption in their approach that the nonlinear stress-strain behavior can be represented by a power-law function. Specifically, they choose the following equation to represent plastic strain (ϵ_p) as a function of stress (σ)

$$\frac{\epsilon_p}{\epsilon_0} = \alpha \left(\frac{\sigma}{\sigma_0} \right)^n \quad (3.5)$$

in which n is the hardening exponent and α is a material constant. The constants σ_0 and ϵ_0 are reference values of stress and strain. If σ_0 and ϵ_0 are chosen such that:

$$\sigma_0 = E \epsilon_0 \quad (3.6)$$

then the total strain (ϵ) can be written as

$$\frac{\epsilon}{\epsilon_0} = \frac{\sigma}{\sigma_0} + \alpha \left(\frac{\sigma}{\sigma_0} \right)^n \quad (3.7)$$

With plastic strain expressed as in Equation (3.5), Kumar et al. (Ref. 40) exploit the Ilyushin's theorem (Ref. 41) to express the plastic component of the crack-opening displacement (δ_p) for a circumferential crack in a pipe subjected to remote bending as

$$\delta_p = \alpha \epsilon_0 a h_2 \left(\frac{a}{b}, n, \frac{R}{t} \right) \left(\frac{M}{M_0} \right)^n \quad (3.8)$$

where M and M_0 respectively represent the remote moment and the limit moment based on σ_0 as the collapse stress. For the case of a pipe with a circumferential crack subjected to pure tension, M and M_0 would be replaced by the axial force P and the limit load (P_0), respectively. With reference to Figure 3.1,

$$\begin{aligned} a &= \theta R \\ \text{and } b &= \pi R \end{aligned} \quad (3.9)$$

In Equation (3.8) h_2 is a nondimensional function of a/b , R/t and the hardening exponent n . In Ref. 42, h_2 values are given for pipes with circumferential cracks subjected to axial tension and to bending loads. These values were generated using a number of FEM analyses based on a thin-shell formulation. Ref. 42 also gives the elastic component of the COD (δ_e). This is expressed as follows:

$$\delta_e = \left(\frac{M}{E} \right) \cdot f_2 \left(a_e, \frac{R}{t} \right) \quad (3.10)$$

where f_2 is a nondimensional function of the effective crack length (a_e) and the radius-to-thickness ratio of the pipe. The effective crack length is defined as:

$$a_e = a + \frac{1}{2\pi} \left[\frac{n-1}{n+1} \right] \left(\frac{K}{\sigma_0} \right)^2 \quad (3.11)$$

For a pipe under axial tension, the term M in Equation (3.10) is replaced by P . Ref. 42 provides the values of f_2 for pipes subjected to axial tension and bending loads.

Using Equations (3.8) and (3.10), together with the tabulated h_2 and f_2 values given in Ref. 42, one can find the total COD (δ) as the sum of δ_p and δ_e .

However, this still leaves the problem of determining the ACO. Ref. 42 does not provide any information on crack-opening profiles which is needed in order to calculate the ACO.

In their adaptation of the Ref. 42 method to calculate ACO, Norris et al. (Ref. 8) assume the crack shape to be elliptical, diamond-shaped, or rectangular (as shown in Figure 3.1). Thus, knowing δ and the crack length ($2a$), the corresponding ACO is readily found. Note that for the same δ and crack length, the calculated ACO would be twice as much for a rectangular crack as for a diamond-shaped crack.

Despite the ambiguity in inferring ACO from the estimated δ values, the method adopted by Norris et al. (Ref. 8) represents the state-of-the-art in EPFM estimation models. It is the only currently available method that we know of which can be used to address problems ranging from small-scale yielding to net-section yielding extremes. Also, as far as we know, it is the only method which takes into account strain hardening of the material, albeit in an approximate way.

3.3 Summary of the Area of Crack-Opening (ACO) Models

The available ACO estimation methods can be classified as based on LEFM, pseudoplastic, and EPFM analysis methods. For adaptation in a relatively general LBB analysis procedure, an EPFM estimation model would be most appropriate. The only currently available model that we are aware of in this category is based on Ref. 42, which has been adopted in the PICEP leak-rate estimation computer code developed by Norris et al. (Ref. 8). A shortcoming of this model is that it requires the user to assume the crack-opening profile.

4.0 THE SQUIRT COMPUTER PROGRAM

SQUIRT (Seepage Quantification of Upsets In Reactor Tubes) is a computer program which can be used to predict the area of crack-opening and/or the leakage flow rate for cracked pipe. In this section, the general capabilities of the SQUIRT computer program are discussed. The thermal-hydraulic and area of crack opening model used in the program are described and validated by comparing the model predictions to published experimental data.

The SQUIRT computer program is really the result of combining two independent computer programs. The fracture mechanics analysis performed by SQUIRT was derived from a modified version of the NRCPIPE computer program (Ref. 43). The thermal-hydraulic model was derived from programming the Henry-Fauske model for two-phase flow. A user of the SQUIRT program can choose to run the fracture mechanics or thermal-hydraulic models independently; a combined analysis is also possible. In the combined fracture and fluid mechanics analysis, the user first does the fracture mechanics analysis to yield crack-opening displacements (CODs) and crack lengths as a function of pipe load, and then chooses the specific cases for which leak-rate analyses will be performed. In the combined analysis mode, the user may also input the leakage flow rate and have the computer determine the crack size (COD and crack length) that corresponds to the given leakage flow rate.

4.1 The Thermal-Hydraulic Model

The Henry-Fauske model of two-phase flow through long channels is the basis for the thermal-hydraulic analysis (Refs. 1 and 2). Henry's mass flux equation is written in the following format:

$$\Psi(G_c, p_c) = G_c^2 - \frac{1}{\left[\frac{X_c v_{gc}}{\gamma_o p_c} - (v_{gc} - v_{Lc}) N \frac{dX_E}{dp} \right]_c} = 0, \quad (4.1)$$

subject to the constraint

$$\Omega(G_c, p_c) = p_c + p_e + p_a + p_f + p_k + p_{aa} - p_o = 0, \quad (4.2)$$

where the nonequilibrium vapor generation rate is given by

$$X_c = NX_E \{1 - \exp [-B (L/D_i - 12)]\} \quad (4.3)$$

with the equilibrium quality defined by

$$X_E = \left[\frac{S_o - S_{Lc}}{S_{gc} - S_{Lc}} \right] \quad (4.4)$$

and

$$N = 20 X_E \text{ for } X_E < 0.05 \quad (4.5)$$

$$N = 1.0 \text{ for } X_E \geq 0.05$$

The constant B was inferred from the data of Henry (Ref. 1) as being equal to 0.523. The terms p_e , p_f , p_k , and p_a , and p_{aa} are the pressure losses due to the entrance effects, the friction effects, the crack flow path losses, the acceleration of the fluid, and the crack cross section area changes, respectively. Each of these terms is defined below.

The entrance pressure losses, p_e , are given by

$$p_e = \frac{G_o^2 v_{Lo}}{2 C_D^2} \quad (4.6)$$

where C_D is the coefficient of discharge. The coefficient of discharge is an input variable to the SQUIRT computer program. A value of $C_D = 0.95$ is recommended for tight cracks, i.e., cracks with CODs less than 0.15 mm (0.006 inch). This is in accordance with the ASME Fluid Metering Handbook (Ref. 44) which defines a rounded entrance as one having a rounded inlet equal to 1/6 the radius of the tube opening. Since the hydraulic radius of a tight crack is approximately equal to the COD, the entrance edges would only need to have a radius of 1/6 the COD to be considered round. In the case of a crack with a COD of 0.15 mm (0.006 inch), the radius of the entrance edges would have to be about 0.025 mm (0.001 inch) to be considered round. For cracks with larger CODs, a coefficient of discharge between 0.62 and 0.95 should be chosen based on the judgment of the user as to how round the entrance edges are in comparison to the COD.

The pressure losses due to friction, p_f , are calculated over the flow path length using

$$p_f = \left(f \frac{L}{D_i} \right) \frac{\bar{G}^2}{2} [(1-\bar{X}) \bar{v}_L + \bar{X} \bar{v}_g] \quad (4.7)$$

where the friction factor f is calculated by (Ref. 14)

$$f = [C_1 \log \left(\frac{D_i}{k} \right) + C_2]^{-2}$$

where now the coefficients C_1 and C_2 are equal to 2.00 and 1.14, respectively, for $(D_i/k) > 100$, and 3.39 and -0.866, respectively, for $(D_i/k) < 100$. In the equations that follow, G_s is the average mass flux in the single-phase region, $0 < L/D_i < 12$, of the crack and G_T is the average mass flux in the two-phase region, $L/D_i > 12$, of the crack.

The pressure losses due to bends and protrusions in the crack flow path, p_k , are given by

$$p_k = (e_v) \frac{\bar{G}^2}{2} [(1-\bar{X}) \bar{\nu}_L + \bar{X} \bar{\nu}_g] \quad (4.9)$$

where e_v is the total loss coefficient over the crack flow path length. The variable e_v can be determined experimentally by defining

$$e_v = e [L] \quad (4.10)$$

where e is the number of velocity heads lost per unit flow path length for a given type of crack. A best curve fit to the experimental data reported herein from a fatigue crack in a girth weld suggests a value of e equal to 6 velocity heads per mm of crack flow path.

The phase change acceleration pressure loss, p_a , of the fluid as it flows through the crack is given by

$$p_a = \bar{G}_T^2 [(1-X_c) \nu_{Lc} + X_c \nu_{gc} - \nu_{Lc}] \quad (4.11)$$

Likewise the area change acceleration pressure loss, p_{aa} , of the fluid is given by

$$p_{aa} = \frac{G_{Lo}^2}{2} \left[\left(\frac{A_c}{A_i} \right)^2 - \left(\frac{A_c}{A_o} \right)^2 \right] + \frac{G_c^2}{2} + [(1-X) \nu_{Lc} + X \nu_{gc}] \left[1 - \left(\frac{A_c}{A_i} \right)^2 \right] \quad (4.12)$$

This completes the definition of all the pressure loss terms in Equation (4.1). For more details concerning the derivation of the pressure loss equations the reader is referred to the papers of Henry (Ref. 1), Collier et al. (Ref. 3), Abdollahian et al. (Ref. 45), and the book by El-Wakil (Ref. 46).

Equations (4.1) and (4.2) represent two nonlinear equations with two unknowns, namely G_c^2 and p_c . A Newton-Raphson iteration method [47] was used to solve these simultaneous, nonlinear equations. In the Newton-Raphson technique, an initial guess at G_c^2 and p_c is made (SQUIRT does this automatically unless the user overrides this option), and subsequent guesses are given by

$$[G_c^2]_i = [G_c^2]_{i-1} + h_a \quad (4.13)$$

$$[p_c]_i = [p_c]_{i-1} + h_b \quad (4.14)$$

where

$$h_a = - \frac{\begin{vmatrix} \psi_o & \left(\frac{\partial \psi}{\partial p_c}\right)_o \\ \Omega_o & \left(\frac{\partial \Omega}{\partial p_c}\right)_o \end{vmatrix}}{\frac{\partial (\psi, \Omega)_o}{\partial (G_c^2, p_c)}} \quad (4.15)$$

and

$$h_b = - \frac{\begin{vmatrix} \left(\frac{\partial \psi}{\partial G_c^2}\right)_o & \psi_o \\ \left(\frac{\partial \psi}{\partial G_c^2}\right)_o & \Omega_o \end{vmatrix}}{\frac{\partial (\psi, \Omega)_o}{\partial (G_c^2, p_c)}} \quad (4.16)$$

The Jacobian of ψ and Ω is defined as

$$\frac{\partial (\psi, \Omega)_o}{\partial (G_c^2, p_c)} = \left[\frac{\partial \psi}{\partial G_c^2} \frac{\partial \Omega}{\partial p_c} - \frac{\partial \psi}{\partial p_c} \frac{\partial \Omega}{\partial G_c^2} \right]_o \quad (4.17)$$

These iterations continue until the values of G_c^2 and p_c are correct to within four significant figures. The Newton-Raphson iteration technique usually converges to the desired solution in less than ten iterations but occasionally more are required. Numerical instabilities in the iterative solution are relatively rare (about 1 case in 50), but even these problems can usually be solved by carefully choosing values for the initial guesses for G_c^2 and p_c based upon a graphical interpolation between boundaries where the solution is stable.

4.2 Comparisons of the SQUIRT Thermal-Hydraulic Model With Published Data

A search of the literature for leakage flow rate data for high-temperature and high-pressure water flowing through tight cracks (i.e., cracks with a wall thickness much larger than the COD) was conducted. The data can be divided into three categories: pipe flows, flows through artificially produced slits, and flows through naturally occurring pipe cracks.

Although the literature abounds with data on two-phase flow through long pipes, this particular geometry is not of great interest for flow through tight cracks. However, it is worthwhile determining that the thermal-hydraulic model agrees reasonably well with this well-defined flow situation. For this reason, we limited ourselves to the pipe flow data of Sozzi and Sutherland (Ref. 48). Figure 4.1 compares the SQUIRT thermal-hydraulic predictions to the pipe flow data of Sozzi and Sutherland. The agreement between the model and the experiment is excellent.

The next level of difficulty involves comparing the thermal-hydraulic model to the experimental data obtained on artificially produced slits with known surface roughness and spacing. Figures 4.2, 4.3, and 4.4 show the comparison of the SQUIRT thermal-hydraulic model to the slit flow data of Collier et al. (Ref. 3), Amos et al. (Ref. 49), and Yano et al. (Refs. 50 and 51). In all instances, the agreement is not quite as good as that for pipe flows. In general, the SQUIRT program predicts leakage flow rates that are lower than those measured with artificial slits. Since the geometry is well-defined, this suggests that the nonequilibrium vapor generation rate may be different for flow through tight slits than for flow through long tubes. Allowing less vapor to be formed in the slit would increase the net flow through the artificial slits. As mentioned previously in Section 2.1.4, Jones (Ref. 21) and the French researchers (Ref. 22) have information that support this hypothesis. Additional work is needed in this area.

The final level of difficulty involves the comparison between the thermal-hydraulic models and the leakage flow rate data obtained in naturally occurring pipe cracks. Figures 4.5 through 4.8 compare the SQUIRT thermal-hydraulic model predictions with the experimental data for intergranular stress corrosion cracks obtained by Collier et al. (Ref. 3). In general there is a much greater scatter in the data than previously seen in either the pipe or slit flow tests. For the smallest COD of 20 μm (Figure 4.7), the calculated flow rate exceeds the measured flow rate by factors of 3 to 8.

Figure 4.1 Comparison of SQUIRT thermal hydraulic model predictions with the experimental data of Sozzi and Sutherland (Ref. 48).

(a) Gallons per minute for water at ambient conditions.

(b) Calculation Error, % = predicted minus measured flow rate divided by measured flow rate times 100.

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Figure 4.2 Comparison of SQUIRT thermal hydraulic model predictions with the experimental data of Collier et al. (Ref. 3) for flows through artificially produced tight slits.

(a) Gallons per minute for water at 1 atm and 20 C.

(b) Calculation Error, % = predicted minus measured flow rate divided by measured flow rate times 100.

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Figure 4.3 Comparison of SQUIRT thermal hydraulic model predictions with the experimental data of Amos and Schrock (Ref. 49) for flows through artificially produced slits.

(a) Gallons per minute for water at 1 atm and 20 C.

(b) Calculation Error, % = predicted minus measured flow rate divided by measured flow rate times 100.

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Figure 4.4 Comparison of SQUIRT thermal hydraulic model predictions with the experimental data of Matsushima et al. (Ref. 50) for flows through artificially produced slits.

(a) Gallons per minute for water at 1 atm and 20 C.

(b) Calculation Error, % = predicted minus measured flow rate divided by measured flow rate times 100.

Figure 4.5 Comparison of SQUIRT thermal hydraulic model predictions with the experimental data of Collier et al. (Ref. 3) for cracks with a COD of 220 μm .

(a) Gallons per minute for water at 1 atm and 20 C.

(b) Calculation Error, % = predicted minus measured flow rate divided by measured flow rate times 100.

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Figure 4.6 Comparison of SQUIRT thermal hydraulic model predictions with the experimental data of Collier et al. (Ref. 3) for cracks with a COD of 108 μm .

(a) Gallons per minute for water at 1 atm and 20 C.

(b) Calculation Error, % = predicted minus measured flow rate divided by measured flow rate times 100.

Figure 4.7 Comparison of SQUIRT thermal hydraulic model predictions with the experimental data of Collier et al. (Ref. 3) for cracks with a COD of 50 μm .

(a) Gallons per minute for water at 1 atm and 20 C.

(b) Calculation Error, % = predicted minus measured flow rate divided by measured flow rate times 100.

Figure 4.8 Comparison of SQUIRT thermal hydraulic model predictions with the experimental data of Collier et al. (Ref. 3) for cracks with a COD of 20 μm .

(a) Gallons per minute for water at 1 atm and 20 C.

(b) Calculation Error, % = predicted minus measured flow rate divided by measured flow rate times 100.

Collier et al. (Ref. 3) have attributed this larger uncertainty to the possibility that the crack could have become partially plugged by particles in the water. For the larger CODs, the SQUIRT program tends to agree reasonably well with the measured data points, although there is much more scatter in the data than observed for the artificial slit experiments.

4.3 The Area of Crack-Opening (ACO) Model

Based on the review of ACO estimation methods described in Section 3.0, the EPFM model adopted in the PICEP computer code of Norris et al. (Ref. 8) may be the most appropriate in a general LBB analysis procedure. This conclusion was based on two main reasons. First, the EPFM model includes, as limiting cases, the small-scale yielding and the net-section yielding extremes of crack-tip plastic deformation. For situations in between these extremes, the model provides the only available ACO estimation method. Second, the model is consistent with the widely accepted EPRI/GE methodology (Ref. 40) for elastic-plastic fracture mechanics assessment of the load-carrying capacity of nuclear power plant piping. In the following we describe the adaptation of the EPFM in the SQUIRT computer code.

4.3.1 The ACO Estimation Model in SQUIRT

The area of crack opening (ACO) estimation in SQUIRT is performed using Equations (3.8) and (3.10) to determine, respectively, the plastic component (δ_p) and the elastic component (δ_e) of the COD. The total COD (δ) is the sum of the elastic and the plastic components, that is

$$\delta = \delta_e + \delta_p \quad (4.18)$$

Knowing δ , the ACO is estimated by assuming the crack-opening profile as an ellipse. The values of the nondimensional function h_2 and the expression for the limit moment, M_0 , which are needed to find δ_p , were taken from Kumar et al. (Ref. 42). The determination of δ_e in SQUIRT differs slightly from the Ref. 42 method. In SQUIRT, the function f_2 is evaluated for the actual crack length rather than at the effective crack length value given by Equation (3.11). The rationale for using a_e instead of a in Ref. 42 is that for $M \ll M_0$ and $n \gg 1$, the plastic component δ_p [see Equation (3.8)] will tend to be underestimated. Therefore, the attempt is to account for this underestimate by using a plastic-zone correction in the elastic component, δ_e . However, recent work performed by Battelle (Ref. 52) indicates that the Ref. 8 method tends to overestimate experimental displacement values even when the actual (rather than effective) crack length is used in the calculations. Therefore, in the present work, it was decided to evaluate the h_2 functions using the actual crack length.

In Ref. 42, the tabulated values of h_2 and f_2 functions are provided for specific a/b , n and R/t . In SQUIRT, a linear interpolation of these functional values is performed to estimate the functional values appropriate for a given problem. The program requires the initial crack length, pipe dimensions, and the constants in the (Ramberg-Osgood) Equation (3.7) as input. For cases in which crack growth is involved, the program also requires the J-resistance curve of the material to be input. Crack growth analysis is performed using

the J-integral based tearing instability theory approach. A detailed description of this approach can be found in Ref. 40.

4.3.2 Accuracy Assessment of the ACO Model

The ACO model in SQUIRT was subjected to accuracy assessment by comparing its estimates with available experimental measurements as well as with available elastic-plastic finite element method (FEM) results. The experimental data used for comparisons were in the form of COD measurements made during pipe fracture experiments previously conducted at Battelle. One of these experiments, conducted on a 406-mm (16-inch) diameter Type 304 stainless steel pipe at room temperature, was part of an EPRI-funded effort. The rest of the experimental data were obtained in the NRC-funded Degraded Piping Program--Phase II (DP³II), Ref. 53. The elastic-plastic finite element method (FEM) results were all developed in the DP³II, Ref. 53. Table 4.1 contains a summary of the experimental and FEM results used in the accuracy assessment of the ACO model. As indicated, the accuracy assessment involved a wide range of pipe sizes, temperatures, and carbon steel as well as welded and unwelded stainless steel pipes. The detailed information on the experiments and the related material property data can be found in the references listed in the last column of Table 4.1.

A key input to the ACO estimation model is a description of the stress-strain curve of the material in terms of the constants of Equation (3.7). These constants are obtained by fitting the curve represented by Equation (3.7) to experimental uniaxial stress versus strain data. For example, the stress-strain data for Type 304 stainless steel at room temperature (Experiment 8T in Table 4.1) are shown in Figure 4.9. The problem is that many of the data defy description using a single power-law equation. Depending upon the strain range selected to best fit the equation, one can obtain widely different values of the constants in Equation (3.7). For example, Figure 4.9 shows three possible curve fits depending upon whether one tries to obtain a good fit in the low strain range, high strain range, or the total strain range. The last of the three, the total strain range fit, was obtained using a linear regression analysis of the stress-strain data.

To judge which of the three curve fits should be used in an ACO estimation by SQUIRT, results for COD were obtained using all three curve fits. The results corresponding to the load at crack growth initiation in Experiment 8T are shown in Figure 4.10. Also shown in the figure are the experimental measurements and the elastic-plastic FEM results which give the crack-opening profile. It is seen that while all three stress-strain fits provide a reasonably good estimate of the center crack-opening displacement, the high-strain fit gives the result closest to the stainless-steel, cracked-pipe experimental data.

In Figure 4.10 we also show the three crack shape options (diamond, ellipse and rectangle) used in the PICEP code (Ref. 8). Compared to the experimental data as well as the finite element results, the ellipse is found to best represent the crack-opening shape. Note that in the finite element analysis, the stress-strain curve of the material was described by a multilinear representation of the experimental data.

Table 4.1 Experiments and FEM analyses used in the ACO model accuracy assessments.

Experiment Number	Pipe Diameter mm (inch)	Pipe Thickness mm (inch)	Material		Temperature C (F)	FEM Results Available	Reference
			Base	Weld Type			
8T	406 (16.0)	26.20 (1.03)	SA-358 TP 304 Stainless Steel	--	21 (70)	Yes	55
4111-1	114 (4.5)	8.9 (0.35)	SA-333 Grade 6 Carbon Steel	--	288 (550)	No	52
4111-2	711 (28.0)	23.6 (0.93)	A-155-KC60 -CL1 Carbon Steel	--	288 (550)	No	52
4111-3	1067 (42.0)	7.1 (0.28)	SA-358 TP 304 Stainless Steel	--	4 (40)	No	52
4111-4	1067 (42.0)	15.9 (0.625)	API 5LX65	--	-7 (20)	No	52
4141-1	152 (6.0)	14.3 (0.562)	SA-376 TP 304 Stainless Steel	SAW	288 (550)	No	56
4141-3	406 (16.0)	26.2 (1.03)	SA-358 TP 304 Stainless Steel	SAW	288 (550)	Yes	56

Figure 4.9 Engineering stress-strain curve for Type 304 stainless steel pipe (Experiment 8T) using a high strain, low strain, and total strain (linear regression) fit.

T5128-F4.9

Figure 4.10 Crack-opening displacement at the load at crack initiation in Experiment 8T.

T5128-F4.10

In Figure 4.11 we show the results of the ACO estimation model for Experiment 4111-1. As indicated in Table 4.1, the experiment in this case was performed on 114-mm (4.5-inch) diameter SA-333, Grade 6, carbon steel pipe. The solid line in this figure represents the measured COD as a function of applied load up to the load at crack initiation. It is seen that in this case, the linear regression fit of the stress-strain data over the whole strain range leads to the best estimate of COD. The same trend was also found to be true for Experiment 4111-3, the results of which are shown in Figure 4.12. However, in this case the results using the low strain and the total strain curve fits were virtually the same. As indicated in Table 4.1, the material in pipe Experiment 4111-3 was Type 304 stainless steel. Reviewing the estimation method results of Figures 4.10, 4.11 and 4.12, it would appear that the use of a linear regression curve fit of the entire stress-strain curve may serve as the method for prescribing the constants of Equation (3.7) in SQUIRT. Figure 4.13 shows the result of the estimation analysis using this method for Experiment 4111-2 of Table 4.1. Again, the calculated CODs are in reasonably good agreement with experimental data up to the load at crack initiation.

The accuracy assessment described thus far was performed using experimental data and FEM results for through-wall circumferential cracks in unwelded pipes subjected to bending. In a large number of practical applications, the ACO estimation model can be expected to be used in leak-before-break (LBB) assessments of cracks in welds. Therefore, it is worthwhile to investigate how well the model performs in such cases. This was done by using experimental data

from the DP3II, Experiments 4141-1 and 4143-3. As indicated in Table 4.1, these experiments involved 152 mm (6.0 inch) and 406 mm (16.0 inch) Type 304 stainless steel pipes with submerged-arc welds (SAWs). In each case, the crack was located in the weld and the pipes were loaded in four-point bending. COD measurements were made beginning at load application all the way to maximum load.

A difficulty in estimating ACO for cracks in welds is that the model does not allow for explicit representation of the stress-strain curves of the two materials. Therefore, one is restricted to using either the base metal or the weld metal stress-strain curve in the calculation of δ_p . Based on the argument that the pipe's deformation response is governed mainly by the base material stress-strain behavior, in the present investigation the material constants of Equation (3.7) were found using the base material stress-strain data. Thus, up to crack initiation, the model does not recognize that the crack resides in a weld. Crack initiation and subsequent crack growth are, however, assumed to occur following the J-resistance (J-R) curve of the weld material. The stress-strain and J-R curve data needed for the analyses were found in the references listed in Table 4.1.

Figure 4.14 shows the calculated CODs for Experiment 4141-3. The calculations were performed using a linear regression curve fit to the stress-strain data of the base material. The arrow in the figure indicates crack initiation. It is seen that at all load levels the estimation model gives overestimates of the COD. In Figure 4.15 we show the COD values, estimated using three different curve fits to the stress-strain data, at the maximum load reached in Experiment 4141-3. Also included in this figure is the result of elastic-plastic FEM analysis, reported in Ref. 55. While the FEM results are in good agreement with

Figure 4.11 Crack-opening displacement in Experiment 4111-1 up to load at crack initiation.

T5128-F4.11

Figure 4.12 Crack-opening displacement in Experiment 4111-3 up to load at crack initiation.

T5128-F4.12

Figure 4.13 Crack-opening displacement in Experiment 4111-2 up to load at crack initiation.

T5128-F4.13

Figure 4.14 Crack-opening displacement in Experiment 4141-3 using base metal stress-strain curve and weld metal J-R curve.

T5128-F4.14

Figure 4.15 Crack-opening profile at maximum load in Experiment 4141-3.

T5128-F4.15

experimental data, the estimation model results with all three curve fits are significantly lower. Figure 4.15 also shows that as in the unwelded case (Figure 4.10), the crack-opening profile is well represented by an ellipse with the crack length and the center COD as the major and minor axes, respectively.

Significant discrepancy between estimated and experimental COD results was also found in Experiment 4141-1. These results are shown in Figure 4.16. It is seen that for loads higher than 25 percent of the initiation load, the estimation model overestimates the CODs.

4.3.3 Summary of ACO Model Assessment

An accuracy assessment of the ACO estimation model in SQUIRT code was carried out by comparing its results with experimental data as well as with elastic-plastic FEM solutions. The assessment showed that the opening profile of cracks in both welded and unwelded pipes was well represented by an ellipse whose major and minor axes are the crack length and the center COD, respectively. For unwelded pipes, the model gave adequately accurate crack-opening displacement estimates using a linear regression curve fit to determine the constants in the Ramberg-Osgood equation. For pipes with cracks in SAW welds, the model overestimated the crack-opening displacement by large amounts.

Figure 4.16 Crack-opening displacement in Experiment 4141-1.

T5128-F4.16

5.0 LEAK-RATE TESTING

The objective of the leak-rate tests is to provide experimental data that could be used to validate existing leak-rate estimation models. Based upon the needs of leak-before-break (LBB) design analyses, such as those contained in the NRC General Design Criteria 4 of Appendix A to 10 CFR Part 50, the experimental data of most interest are for small cracks with leakage rates of about 4 liters per minute (1 gpm). The experiments described herein were designed to provide experimental data to meet this specification.

A single pipe test specimen containing a fatigue-generated crack was used to obtain the leak-rate experimental data. In order to maximize the amount of data generated from the leak-rate tests, the pipe specimen was mounted in a four-point bending apparatus and subjected to loads within its elastic limit while at the same time varying the internal fluid pressure from 8.3 MPa (1150 psia) to 15.2 MPa (2200 psia). In this manner, data were obtained on the crack-opening displacement (COD) as a function of pipe load and on the leakage flow rate as a function of COD and fluid thermodynamic conditions. Details of the actual test conditions are included in this chapter.

5.1 Pipe Test Specimen

The test specimen used for the leak-rate experiment was a 152-mm (6-inch) nominal diameter Schedule 80 carbon steel pipe with a circumferential through-wall crack in a circumferential girth weld. The pipe material for this test specimen was SA106 Grade B. The pipe section was obtained from the Beaver Valley Whip Jet Program. The crack was generated by machining a shallow circumferential notch on the inside surface of the pipe in the center of the weld using electric discharge machining (EDM) techniques. The defect was then cyclically loaded under four-point bending until a fatigue crack was obtained which penetrated the pipe wall. A sketch of the test specimen and final through-wall crack geometry is shown in Figure 5.1. Table 5.1 provides the physical dimensions of the test specimen and crack geometry, as well as the material properties of the pipe section.

Initially the fatigue crack was very tight. The crack opening at the center of the crack was significantly less than 0.025 mm (0.001 inch). However, while evaluating the clip gage data at the center of the crack, it was found that the test specimen was inadvertently overloaded. As a result, a small amount of plastic deformation was created at the two crack tips so that when the specimen was unloaded, some residual crack opening was present. After the evaluation of the clip gage data, the COD at the center of the crack was determined to be approximately 0.114 mm (0.0045 inch) on the outside of the pipe and 0.0889 mm (0.0035 inch) on the inside of the pipe.

45

T5128-F5.1

Figure 5.1 Sketch of test specimen showing details of final through-wall crack geometry.

Table 5.1 Physical dimensions of test specimen and crack geometry, and material properties of the pipe section.

Pipe Diameter	168 mm (6.625 inch)
Wall Thickness	11.1 mm (0.437 inch)
Depth of Fatigue Crack Through the Thickness	8.28 mm (0.326 inch)
Through-Wall Crack Length	
Inside	35.3 (1.39 inch)
Outside	31.2 (1.23 inch)
Crack Opening Displacement (COD) at No Load	
Inside	0.089 mm (.0035 inch)
Outside	0.114 mm (.0045 inch)
Base Metal Yield Strength*	310 MPa (45.0 ksi)
Base Metal Ultimate Strength*	535 MPa (77.5 ksi)

* From mill test reports

After the testing was complete, the fatigue-cracked pipe was broken open in a brittle manner. Figure 5.2 is a photograph of the fracture surface for this fatigue crack. The initial crack length (2a) was measured post-test to be 35.3 mm (1.39 inch) on the inside surface and 31.2 mm (1.23 inch) on the outside surface. Thus, the average crack length was assumed to be 33.3 mm (1.31 inch). The thickness of the pipe wall and the depth of the initial EDM notch were also measured post-test. The wall thickness was 11.1 mm (0.437 inch) and the depth of the initial EDM notch was 2.82 mm (0.111 inch). Thus, the thickness of the remaining ligament of the fatigue crack was 8.28 mm (0.326 inch). The through thickness surface roughness was also measured at five locations along the crack using a Taly-Surf surface roughness measuring device. The centerline average value of these five roughness measurements was 0.003 mm (119 micro-inch).

Although the surface roughness measurements indicate a relatively smooth surface on a microscopic level, the contours of the crack surface on the macroscopic level were quite pronounced. Around the periphery of the crack, the surfaces appeared visually to be very rough. Only the central region of the crack surface appeared to be visually smooth. Although this is evident in the photograph of the crack surface shown in Figure 5.2, it is even more pronounced when viewing the crack faces with the naked eye. When one considers that the COD is only about 0.1 mm (0.004 inch) wide (under no load conditions), the macroscopic contours on the peripheral surfaces of the crack provide a tortuous path of resistance for the fluid. This added resistance to the fluid flow needs to be taken into account in any analytical models of the crack leakage rate. In future leak-rate studies, a contour map of the crack face would be useful for assessing the flow path resistance. This would provide an

Figure 5.2 Post-test photograph of the fracture surface of the fatigue-generated crack in the girth weld of the pipe test specimen.

T5218-F5.2

experimental basis for choosing the crack-pathway loss coefficient for use in existing analytical models.

5.2 Description of Leak-Rate Facility

Figure 5.3 shows a layout drawing of the facility. The flow facility consists of high-pressure air storage bottles, an electrically heated water supply vessel, a filtering and metering section, and control valves. The water supply vessel and piping are insulated to minimize heat losses. Outdoor piping is wrapped in plastic over the insulation to protect it from rain and from water being discharged from the crack. The pipe specimen was fitted with heater tapes for heating the specimen to test temperatures prior to conducting a test. The particulate filters utilized stainless steel sintered elements to remove all particles larger than 2 microns.

In order to maximize the amount of data we might obtain from a single test, it was decided to measure the leak rates as the specimen was loaded under both internal pipe pressure and four-point bending. By loading the specimen under four-point bending it was possible to obtain a range of ACOs, and thus leak rates, for any set of thermodynamic conditions, that is, water pressure and temperature. The loading frame used for this experiment is shown in Figure 5.4. The inner span between the rams is 1.22 m (48 inches), and the outer span between the restraints is 2.69 m (106 inches).

Instruments used to obtain test data were calibrated prior to their installation into the facility. Thermocouple probes and pressure transducers were calibrated by the Battelle Instrument Laboratory. The turbine meter used for measuring flow rate through the crack was calibrated by the manufacturer, Flow Technology, Inc. The load cells used to measure the applied bending loads were calibrated by Battelle's Fatigue Laboratory. The COD instrument was calibrated at Battelle using a precalibrated traveling micrometer. The micrometer was calibrated by the Battelle Instrument Laboratory. The Battelle Instrument Laboratory maintains a calibration capability which it uses to calibrate its own instruments and others used throughout the Laboratory.

5.3 Test Procedure

The procedure used for conducting leak flow tests entailed charging the air bottles to 24.1 MPa (3500 psia), filling and heating the water supply vessel to 294 C (560 F) at 7.2 MPa (1050 psia), heating the specimen to 294 C (560 F), and zeroing the instrumentation as appropriate.

Carrying out the tests required filling the piping to the specimen with hot, pressurized water, filling the specimen with hot, pressurized water, venting the air from the specimen, and measuring leak flow rates at selected operating conditions. Filling the piping was done by bleeding fluid from the piping through the piping fill valve, allowing flow to continue until the water at the specimen fill valve remained at a constant temperature. The piping fill valve was then closed and the specimen fill valve opened. When the pressure in the specimen equaled the supply pressure, the vent valve was opened to expel the air contained in the specimen. Following this, the flow control valve was

T5128-F5.3

Figure 5.3 Layout drawing of the experimental facility.

T5128-F5.4

Figure 5.4 Schematic of pipe strongback used in leak-rate experiments.

adjusted to obtain the desired pressure in the test specimen and a set of data was recorded. The data were recorded on magnetic disk and simultaneously displayed on a video screen. Multiple scans of data were taken at each operating condition. The pressure in the test specimen was then adjusted to the next desired pressure between 7.2 MPa (1050 psia) and 15.2 MPa (2250 psia), and another set of data was recorded. After all of the data were obtained within the pressure range of interest, the load on the pipe test specimen was increased using the pipe bending apparatus and the tests were repeated.

5.4 Crack-Opening-Displacement (COD) Measurements

The COD device used in this experiment was a horseshoe-shaped clip gage (Figure 5.5) typical of the design used in the Degraded Piping Program (Ref. 52). The clip gage was mounted across the crack in a plane perpendicular to the plane of the crack (Figure 5.5). One of the feet of the clip gage was fixed, by bolting in two places, to one of the two vertical posts welded to the pipe adjacent to the crack. The other was allowed to slide in a V-notch groove machined into the other vertical post.

During the course of the elevated temperature leak-rate test, it appears that the clip gage was bounced quite a bit by the steam jets emanating from the crack. The clip gage signal was very noisy, rendering it useless as far as determining the COD. In order to circumvent this problem, COD data, which were recorded earlier during a series of cold shakedown tests, were used to estimate the COD. During these cold tests, COD data were recorded as a function of internal pipe pressure and the applied bending load.

The major differences in the COD data between the hot tests and the cold tests are the response of the material and the pressure component acting on the crack faces. Since the loadings applied to the cracked pipe specimen during the elevated temperature tests were predominantly elastic, any differences due to the material response should be minimal. As far as differences due to the pressure component acting on the crack faces, during the hot test the two-phase flow through the crack results in a greater pressure drop than would occur during the cold test. As such, the measured COD from the cold test would probably be very slightly greater than the COD for a corresponding hot test. However, for reasons to be addressed below, it is felt that this difference is minimal for this series of tests. Consequently, an engineering assessment of the COD for the hot test can be made from the cold shakedown test data.

Figure 5.6 is a plot of the crack opening as a function of the internal pipe pressure for one of the cold shakedown tests. Initially the crack opened at a rate of approximately 0.0127 mm (0.0005 inch) per 6.9 MPa (1000 psi). After the pressure had been increased to approximately 8.6 MPa (1250 psi) the pressure was dropped to approximately 3.8 MPa (550 psi). During this depressurization, the COD-versus-pressure curve followed a curve parallel to the curve for the initial pressurization. After the pressure was decreased to 3.8 MPa (550 psi), the pipe was repressurized. Up to approximately 6.9 MPa (1000 psi), the repressurization data fell on top of the previous depressurization data. At that time there appeared to be a slight perturbation in the data. Upon further pressurization from 8.3 to 10.3 MPa (1200 to 1500 psi),

Figure 5.5 Schematic of horseshoe style clip gage used to measure crack-opening displacement in leak-rate experiments.

Figure 5.6 Plot of crack-opening displacement as a function of pipe pressure for cold shakedown test.

T5128-F5.6

the slope of the COD-versus-pressure data paralleled the earlier data. Subsequent pressurizations for this same day of testing demonstrated similar behavior, i.e., a COD increase of approximately 0.0127 mm (0.0005 inch) per 6.9 MPa (1000 psi).

Figure 5.7 is a plot of the COD as a function of the total applied bending moment. Except for a perturbation in the data at the start of the test, the crack opened at a rate of approximately 0.033 mm (0.0013 inch) per 20,000 N-m (177,000 inch-pounds) of bending moment.

In addition to the CODs due to the pressure and bending loads, the crack was opened approximately 0.102 mm (0.004 inch) in the unloaded state. The initial unloaded COD was measured post-test to be 0.114 mm (0.0045 inch) on the outside surface and 0.089 mm (0.0035 inch) on the inside surface. Thus, the average unloaded COD was assumed to be 0.102 mm (0.004 inch). This initial 0.102-mm (0.004-inch) opening was the result of a previously applied bending moment. In an attempt to assess the stability and accuracy of the clip gage, a series of four-point bend tests were conducted. The test specimen for this series of tests was at 288 C (550 F) and unpressurized. As the bending moments were applied the COD was measured visually by measuring the distance between two scribe lines adjacent to the crack using a traveling microscope. These measurements were then compared to the output from the clip gage. However, during the course of this testing the specimen was slightly overloaded. As a result, a small amount of plastic deformation occurred at the two crack tips, resulting in the residual 0.102-mm (0.004-inch) opening.

Consequently, the total COD (δ) during the leak-rate experiments was comprised of the initial opening (δ_i) plus the opening due to the pressure (δ_{pr}) and the bending moment (δ_m). Assuming that the total displacement is the sum of these three components, the total displacement is:

$$\delta = \delta_i + \delta_{pr} + \delta_m \quad (5.1)$$

For example, at 13.8 MPa (2000 psi) pressure and 20,000 N-m (177,000 inch-pounds) bending moment, the total displacement (δ) is

$$\delta = \delta_i + \delta_{pr} + \delta_m \quad (5.2)$$

$$\delta = 0.102 \text{ mm} + 0.025 \text{ mm} + 0.033 \text{ mm} = 0.16 \text{ mm} \quad (5.3)$$

$$\delta = 0.0040 \text{ inch} + 0.0010 \text{ inch} + 0.0013 \text{ inch} = 0.0063 \text{ inch} \quad (5.4)$$

For the pressures [up to 13.8 MPa (2,000 psi)] and bending moments [up to 26,200 N-m (232,000 inch-pounds)] applied during the hot tests, the dominant term in the above expression is the initial COD due to a pretest overload. The CODs due to the pressure and bending components appear to be second order terms. As a result, since the pressure term appears to be second order, any errors introduced by using the cold test data will undoubtedly be insignificant.

Figure 5.7 Plot of crack-opening displacement as a function of applied bending moment for cold shakedown test.

T5128-F5.7

5.5 Comparisons of COD Data to Analytical Predictions

As part of this project, an analytical model (the SQUIRT code) was developed for predicting the CODs as a function of applied bending moments. Figure 5.8 shows a comparison between the experimental data and the analytical predictions for the 33.3-mm (1.31-inch) long through-wall crack evaluated in these leak-rate experiments. As can be seen, the agreement between the experimental data and the predictions is reasonable, over the range of bending moments evaluated in the experiments.

5.6 Leak-Rate Measurements

The leakage flow rate through the fatigue crack in the girth weld was measured using a turbine flow meter placed in the flow line feeding the pipe test specimen. Table 5.2 lists the measured flow rates as a function of the fluid thermodynamic conditions and the CODs. The CODs were varied by changing the applied bending moment on the pipe test specimen.

The same data shown in Table 5.2 are also shown plotted in Figure 5.9. Two sets of data were taken under no-load conditions; the first set was taken at the start of testing and the second set was taken at the end of testing after all of the pipe bending tests were completed. The reasonable agreement between the two sets of data indicates that the pipe test specimen did not exceed the elastic limit during the course of the leak-rate tests.

The lines drawn through the data points indicate the best judgment for the straight line fits to the data at each applied level of bending moment. Although there is some scatter in the experimental data, the general trends in the data make sense. As the fluid pressure inside the test specimen is increased, the leakage flow increases as expected. Similarly, as the applied bending moment on the pipe increases and the crack opens up, the leakage flow rate also increases. However, the absolute magnitude of the increased leakage flow rates are lower than expected based on the pretest analytical predictions. The pretest analytical predictions did not take into account the pressure losses associated with the crack pathway loss coefficient. This loss coefficient accounts for pressure losses associated with the fluid having to flow around bends and protrusions in the flow field. The bends and protrusions in the flow field were confirmed to exist during the post-test examination of the fatigue crack surfaces.

Figure 5.10 compares the predicted leakage flow rates from the SQUIRT computer model to the experimental results from the leakage test. In the SQUIRT program, a crack pathway loss coefficient of 6 velocity heads per millimeter of crack wall thickness was found to give the best agreement to the experiment data. If this parameter is set to zero, as was done in the pre-test analysis, the predicted flow rates are high by about a factor of two to three when compared to the experimental data. With the crack pathway loss coefficient included in the analysis, the agreement between the analytical and experimental results is reasonably good for all of the bending moment tests (see Figure 5.10).

Figure 5.8 Comparison between experimental data and analytical predictions for the 33.3-mm (1.31-inch) long through-wall crack evaluated in the leak-rate experiments.

Table 5.2 Listing of measured leakage flow rates for a fatigue crack in a girth weld.

	Bending Moment		Pressure		Temperature		Inside COD		Outside COD		Measured Leakage ^a	
	N·M	(in. lb) x10 ³	MPa	(psia)	C	(F)	μm	(in.) x10 ⁻³	μm	(in.) x10 ⁻³	kg/s	(gal/min)
58	0	(0)	9.04	(1310.8)	273.8	(525)	106	(4.18)	131	(5.16)	0.0339	(.538)
	0	(0)	9.5	(1377.5)	274.8	(527)	106	(4.18)	132	(5.20)	0.0356	(.565)
	0	(0)	11.5	(1667.5)	277.8	(532)	110	(4.33)	135	(5.32)	0.0444	(.704)
	0	(0)	12.5	(1812.5)	278.8	(534)	112	(4.42)	137	(5.40)	0.0467	(.741)
	0	(0)	13.5	(1957.5)	279.8	(536)	114	(4.49)	139	(5.48)	0.0525	(.832)
	0	(0)	14.3	(2073.5)	279.8	(536)	115	(4.53)	141	(5.56)	0.0558	(.885)
	0	(0)	7.92	(1148.4)	272.8	(523)	103	(4.05)	129	(5.08)	0.0300	(.476)
	0	(0)	10.4	(1508.0)	275.8	(529)	108	(4.33)	133	(5.24)	0.0374	(.593)
	0	(0)	12.7	(1841.5)	278.8	(534)	112	(4.42)	138	(5.44)	0.0454	(.720)
	0	(0)	14.9	(2160.5)	280.8	(538)	116	(4.57)	142	(5.59)	0.0635	(1.007)
	13500	(119.5)	8.93	(1294.9)	277.8	(532)	128	(5.04)	153	(6.03)	0.0448	(.711)
	13700	(121.2)	10.5	(1522.5)	279.8	(536)	131	(5.16)	156	(6.13)	0.0508	(.806)
	13800	(122.1)	11.3	(1638.5)	279.8	(536)	132	(5.20)	158	(6.23)	0.0540	(.856)
	13900	(123.0)	14.3	(2073.5)	280.8	(538)	138	(5.44)	164	(6.46)	0.0620	(.983)
	20000	(177.0)	9.91	(1437.0)	278.8	(534)	140	(5.52)	166	(6.54)	0.0540	(.856)
	20100	(177.9)	12.4	(1798.0)	280.8	(538)	145	(5.71)	170	(6.70)	0.0592	(.939)
	20100	(177.9)	12.7	(1841.5)	280.8	(538)	145	(5.71)	171	(6.74)	0.0604	(.958)
	20100	(177.9)	14.4	(2088.0)	281.8	(540)	149	(5.87)	174	(6.86)	0.0651	(1.032)
	25700	(227.4)	7.84	(1136.8)	277.8	(532)	146	(5.75)	171	(6.74)	0.0447	(.709)
	25700	(227.4)	10.3	(1493.5)	279.8	(536)	150	(5.91)	176	(6.93)	0.0562	(.891)
	25900	(229.2)	12.4	(1798.0)	280.8	(538)	154	(6.07)	180	(7.09)	0.0613	(.972)
	25500	(225.7)	13.6	(1972.0)	281.8	(540)	156	(6.15)	181	(7.13)	0.0660	(1.047)

^a Gallons per minute for water at 1 atm and 20°C.

Figure 5.9 Linear fit through experiment leakage flow rate data for the fatigue-generated crack in a girth weld.

(a) Gallons per minute for water at 1 atm and 20 C.

Figure 5.10 Comparison of SQUIRT predicted leakage flow rate calculations with the experimental data obtained for the fatigue-generated crack in a girth weld.

(a) Gallons per minute for water at 1 atm and 20 C.

6.0 CONCLUSIONS AND RECOMMENDATIONS

Review of existing thermal-hydraulic models indicated that a modified form of the Henry-Fauske model (Refs. 1 and 2) for two-phase flow through long tubes is most appropriate for modeling the fluid flow through tight cracks (i.e., cracks with $L/D_i > 12$). The primary uncertainties in the thermal-hydraulic analyses are associated with the flow path losses due to changes in flow direction, the friction factors for flow between two parallel surfaces, the nonequilibrium vapor generation rate for two-phase flow in tight cracks, and the potential for particulates to lodge in the crack, plugging the opening. The importance of each of these parameters has been identified and evaluated as part of this research effort.

The review of the area of crack opening models suggested that the existing models can be broadly classified as LEFM, pseudoplastic, or EPFM. The pseudoplastic models can, in turn, be categorized as small-scale yielding and net-section yielding. In the EPFM category, the only theoretically sound modeling approach is offered by the EPRI/GE estimation model. In principle, the EPRI/GE model includes LEFM and pseudoplastic models as special cases.

A modified form of the Henry-Fauske model (Refs. 1 and 2) for the thermal-hydraulic analysis was used together with the EPFM of EPRI/GE for the fracture mechanics analysis to develop a computer program for leak-rate estimation. The program, called SQUIRT (Seepage Quantification for Upsets In Reactor Tubes), can be used to predict the crack-opening displacement (COD) and leakage flow rate for cracked pipe under a given load. The SQUIRT code has three primary differences with the EPRI PICEP code:

- (1) The SQUIRT code accounts for crack growth by using an elastic-plastic fracture analysis with the J-R curve of the material.
- (2) The SQUIRT code calculates the maximum load using elastic-plastic fracture mechanics, in contrast to the PICEP code, which uses limit-load analysis to calculate the maximum load.
- (3) Both PICEP and SQUIRT can calculate the leakage rate for known loads, crack sizes, thermal-hydraulic conditions, and other pressure losses (e.g., friction factors). However, only the SQUIRT code can directly calculate the crack size for a given load, leakage rate, thermal-hydraulic condition, and pressure loss coefficient.

The SQUIRT computer program was validated by comparing its predictions with published experimental data and with experimental data derived as part of this research effort. The SQUIRT thermal-hydraulic model predictions were compared to experimental data on two-phase flow through long tubes reported by Sozzi and Sutherland (Ref. 48), two-phase flow through slits reported by Collier et al. (Ref. 3), Amos and Shrock (Ref. 49), and Yano (Ref. 50), and two-phase flow through actual cracked pipe reported by Collier et al. (Ref. 3). In general, the SQUIRT program tended to underpredict the experimental data obtained on artificial slits. This was attributed to possibly overestimating the nonequilibrium vapor generation rate for tight slits, although more work needs to be

done to confirm this hypothesis. The SQUIRT program did a reasonable job of predicting the measured leak-rate data of Collier et al. (Ref. 3) in stress-corrosion cracks, but the scatter in the data was much greater than that observed with artificial slits. If the nonequilibrium vapor generation rate were changed to produce good agreement with the artificial slit data, then the SQUIRT program would predict leakage rates too high for the real crack data. However, a crack pathway loss coefficient for stress-corrosion cracks could be determined to provide a best fit to the data under these circumstances. More work is needed to resolve these ambiguities.

The SQUIRT ACO model predictions were compared with experimental data as well as with results of elastic-plastic finite element analyses from the ongoing Degraded Piping Program--Phase II. The test case included circumferential cracks in both carbon steel and austenitic stainless steel pipes, as well as stainless steel submerged-arc welded pipes.

Additional experimental validation data were provided by leak-rate tests performed using a circumferential fatigue crack in a carbon steel pipe with a girth weld. The cracked pipe specimen was mounted into a four-point bending apparatus and the leakage flow rate and COD were measured as a function of the applied bending moment and internal pressure. The applied bending loads were kept below the elastic limit of the cracked pipe. Fluid pressure and temperature were varied to simulate both BWR and PWR thermodynamic conditions. To obtain reasonably good agreement between the experimental results and the SQUIRT predictions for leakage flow rates, a flow path loss coefficient of approximately 6 velocity heads per mm of crack flow path length had to be assumed. This implies that the tight fatigue crack in the girth weld causes the fluid to change direction many times while flowing through the crack. One area recommended for further work is a detailed contour mapping of the crack faces, to verify the validity of this assumption for the flow path for this type of crack. In addition, leak-rate tests should be performed on a variety of real pipe cracks to expand the data base in this area. To date the only other measurements taken on actual cracks in a pipe have been those of Collier et al. (Ref. 3).

The work performed on ACO models determined that for circumferential cracks in unwelded pipes in bending, the EPRI/GE estimation method, combined with the assumption that the crack-opening profile is elliptical, provides adequately accurate estimates. For cracks in welds, the elliptical crack assumption appears to be reasonable; however, there is ambiguity in how one should model the presence of a weld using the EPRI/GE method.

Based on the above observations, it is recommended that additional work be performed to establish a more accurate estimation method for cracks in welds. Furthermore, for both welded and unwelded pipes with small R/t ratios, it is recommended that work be performed to estimate the difference between ACOs on the inside and the outside of the pipe. Since the EPRI/GE method is based on the thin-shell assumption, it can be expected to be accurate only for large R/t ratios. Also, the resulting ACO estimates correspond to the mid-thickness of the pipe.

7.0 REFERENCES

- (1) Henry, R. E., "The Two-Phase Critical Discharge of Initially Saturated or Subcooled Liquid," Nuclear Science and Engineering, Vol. 41, pp. 336-342, (1970).
- (2) Henry, R. E., and Fauske, H. K., "Two-Phase Critical Flow at Low Qualities, Part 1: Experimental," Nuclear Science and Engineering, Vol. 41, pp. 79-91, (1970); "Part II: Analysis," Nuclear Science and Engineering, Vol. 41, pp. 92-98, (1970).
- (3) Collier, R. P., Stulen, F. B., Mayfield, M. E., Pape, D. B., and Scott, P. M., "Two-Phase Flow Through Intergranular Stress Corrosion Cracks and Resulting Acoustic Emission," EPRI Report No. NP-3540-LD, (1984).
- (4) Wallis, G. B., One-Dimensional Two-Phase Flow, McGraw-Hill, (1969).
- (5) Isbin, H. S., Moy, J. E., and Dacruz, J. R., "Two-Phase, Steam-Water Critical Flow," AI. Ch.E. Journal, Vol. 3, pp. 361-365, (1957).
- (6) Zivi, S. M., "Estimation of Steady-State Steam Void Fraction by Means of the Principle of Minimum Entropy Production," Trans. ASME, J. Heat Transfer, Vol. 64, pp. 247-252, (1964).
- (7) Moody, F. J., "Maximum Flow Rate of a Single Component, Two-Phase Mixture," Trans. ASME, J. Heat Transfer, Vol. 86, pp. 132-142, (1965).
- (8) Norris, D. et al., "PICEP: Pipe Crack Evaluation Program," Electric Power Research Institute Report, NP-3596-SR, (1984).
- (9) PICEP: Pipe Crack Evaluation Program (Revision 1), EPRI Report No. NP-3596-SR, (1987).
- (10) Bouré, J. A., "The Critical Flow Phenomenon with Reference to Two-Phase Flow and Nuclear Reaction Systems," ASME Symposium on the Thermal and Hydraulic Aspects of Nuclear Reactor Safety, Vol. 1, pp. 195-216, (1977).
- (11) Ishii, M., Thermo-Fluid Dynamics Theory of Two-Phase Flows, (1975).
- (12) Drew, D. A., "Mathematical Modeling of Two-Phase Flow Models and Their Applicability to Nuclear Reactor Transient Analysis," EPRI Report No. NP-143, Vols. 1, 2, and 3, (1983).
- (13) Ramshaw, J. D., and Trapp, J. A., "Characteristics, Stabilities, and Short Wavelength Phenomena in Two-Phase Flow Equation Systems," Nuclear Science and Engineering, Vol. 66, pp. 93-102, (1978).
- (14) Schlichting, H., Boundary Layer Theory, McGraw-Hill, (1968).

- (15) Schiller, L., "Über den Strömungswiderstand von Rohren verschiedenen Querschnitts-und Rauigkeitsgrades," ZAMM, Vol. 3, pp. 2-13, (1923).
- (16) Nikuradse, J., "Turbulente Strömung in nicht kreisförmigen Rohren," Ing. Arch., Vol. 1, pp. 306-332, (1930).
- (17) Brighton, J. A., and Jones, J. B., "Fully Developed Turbulent Flow in Annuli," J. Basic Eng., Vol. 86, pp. 835-844, (1966).
- (18) Button, B. L., Grogan, A. F., Chivers, T. C., and Manning, P. T., "Gas Flow Through Cracks," J. Fluids Engng., Vol. 100, pp. 453-458, (1978).
- (19) John, H., Reimann, J., Westphal, F., and Friedel, L., "Critical Two-Phase Flow Through Rough Slits," J. Multiphase Flow, (1987).
- (20) Henry, R. E., "Study of One- and Two-Component, Two-Phase Critical Flows at Low Qualities," ANL-7430, Argonne National Laboratory, (1968).
- (21) Jones, O. C., "Flashing Inception in Flowing Liquids", Nonequilibrium Interfacial Transport Processes, pp 29-34, (1979).
- (22) Jones, O. C., Jr., and Zuber, N., "Bubble Growth in Variable Pressure Fields," Trans. ASME, Journal of Heat Transfer, Vol. 100, pp 453-459, (1978).
- (23) Danley, P. J. and MacFarlane, B. J., "Examination of Radioactive Particles in WSGHWR coolant circuits Using Scanning Electron Microscope Autoradiograph," Water Chemistry of Nuclear Reactor Systems, British Nuclear Energy Society, pp 257-264, Bournemouth, England, (1977).
- (24) Moroto, A. J., Blesa, M. A., Regazzoni, A. E., and Urrutia, G. A., "The Synthesis of Corrosion Products for Simulation Studies," Water Chemistry II, British Nuclear Energy Society, pp. 241-245, (1980).
- (25) Vanbrabant, R., and de Regge, P., "Characterization of the Corrosion Products in Primary Reactor Water of PWR During Normal Operation and Transient Phases," Water Chemistry II, British Nuclear Energy Society, pp. 279-284, (1980).
- (26) Comley, G. C., and Bird, E. J., "Operational Experience and Experimental Work on the Winforth Reactor Relating to Activity Retention on Coolant Circuit Surfaces," Water Chemistry II, British Nuclear Energy Society, pp.333-345, (1980).
- (27) Johnson, A. B., Griggs, B., Kustas, F. M., and Shaw, R. A., "Nature of Deposits on BWR and PWR Primary System Surfaces--Relation to Decontamination," Water Chemistry II, British Nuclear Energy Society, pp. 389-394, (1980).
- (28) Rahn, F. J., Adamantiades, A. G., Kenton, J. E., and Brau, C., A Guide to Nuclear Power Technology, John Wiley and Sons, (1984).

- (29) Erdogan, F., "Fracture Analysis of Pipelines Containing Circumferential Flaws," Second Annual Report, U. S. Department of Transportation, Contract No. DOT-RC-82007, (June 1980).
- (30) Erdogan, F., and Delale, F., "Ductile Fracture of Pipes and Cylindrical Containers with a Circumferential Flaw," Trans. ASME, Journal of Pressure Vessel Technology, Vol. 103, pp 160-168, (1981).
- (31) Sanders, J. L., Jr., "Circumferential Through-Cracks in Cylindrical Shells Under Tension, " Journal of Applied Mechanics, Vol. 49, pp. 103-107, (March 1982).
- (32) Sanders, J. L., Jr., "Circumferential Through-Cracks in Cylindrical Shells Under Tension, " Journal of Applied Mechanics, Vol. 50, pp. 221, (March 1982).
- (33) Tada, H., Paris, P. C., and Irwin, G. R., The Stress Analysis of Cracks Handbook, Del Research Corp., Hellertown, (1973).
- (34) Wuthrich, C., "Crack Opening Areas in Pressure Vessels and Pipes," Engineering Fracture Mechanics, Vol. 18, No. 5, (1983).
- (35) Irwin, G. R., Handbuch der Physik VI, Flugge Ed., Springer, pp 551-590, (1958).
- (36) Dugdale, D. S., "Yielding of Steel Sheets Containing Slits," Journal of Mechanics and Physics of Solids, Vol. 8, (1960).
- (37) Paris, P. C., and Tada, H., "The Application of Fracture Proof Design Methods Using Tearing Instability Theory to Nuclear Piping Postulating Circumferential Through-Wall Cracks," NUREG/CR-3464, (1983).
- (38) Klecker, R., Brust, F., and Wilkowski, G., "NRC Leak-Before-Break (LBB.NRC) Analysis Method for Circumferentially Through-Wall Cracked Pipes Under Axial Plus Bending Loads," NUREG/CR-4572, (1986).
- (39) Smith, E., "The Opening of Through-Wall Cracks in BWR Coolant Lines Due to the Application of Severe Overloads," NUREG/CP-0051, (August 1984).
- (40) Kumar, V., German, M. D., and Shih, C. F., "An Engineering Approach to Elastic-Plastic Fracture Analysis," Electric Power Research Institute Report, NP-1931, (1981).
- (41) Ilyushin, A. A., "The Theory of Small Elastic Plastic Deformations," Prikladnaia Matematika i Mekhanika, P.M.M., Vol. 10, (1946).
- (42) Kumar, V. et al., "Advances in Elastic Plastic Fracture Analysis," Electric Power Research Institute Report, NP-3067, (1984).

- (43) Brust, F. W., "Approximate Methods for Fracture Analyses of Through-Wall Cracked Pipe", NUREG/CR-4583, (February 1987).
- (44) Fluid Meters: Their Theory and Application, American Society of Mechanical Engineering, 6th edition, (1971).
- (45) Abdollahian, D., and Chexal, B., "Calculation of Leak Rates Through Cracks in Pipes and Tubes, EPRI Report No. NP-3395, (1983).
- (46) El-Wakil, M. M., Nuclear Heat Transport, International Textbook Company, (1971).
- (47) Hildebrand, F. B., Advanced Calculus for Applications, Prentice-Hall, (1962).
- (48) Sozzi, G. L., and Sutherland, W. A., "Critical Flow of Saturated and Subcooled Water at High Pressure," NEDO-13418, (1975).
- (49) Amos, C., and Shrock, V., "Critical Discharge of Initially Subcooled Water Through Slits," NUREG/CR-3475, (1983).
- (50) Matsushima, E., Yano, T., and Okamoto, A., "Experimental Study of Leak Flow Rate Through Artificial Slits," SMIRT-9, Vol. G, pp 287-292, (1987).
- (51) Yano, T., Matsushima, E., and Okamoto, A., "Leak Flow Rate From Through-Wall Crack in Pipe," Proceedings of 1987 ASME/JSME Thermal Engineering Joint Conference, Honolulu, Hawaii, pp 301-308, (1987).
- (52) Scott, P. and Brust, F., "An Experimental and Analytical Assessment of Circumferential Through-Wall Cracked Pipes Under Pure Bending," Battelle Columbus Division report to NRC, NUREG/CR-4574, (1986).
- (53) Wilkowski, G. M. et al., "Degraded Piping Program -- Phase II - Summary of Technical Results and Their Significance to Leak-Before-Break and In-Service Flaw Acceptance Criteria," NUREG/CR-4082, Volume 8, (March 1989).
- (54) Kanninen, M. F. et al., "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).
- (55) Ahmad, J. A., Nakagaki, M., Brust, F., and Wilkowski, G. M., "Elastic-Plastic Finite Element Crack Growth in Large Compact Tension and Circumferentially Cracked Pipe Specimen," NUREG/CR-4573, (October 1986).
- (56) Wilkowski, G. M. et al., "Analysis of Stainless Steel Flux Weld Experiments," NUREG/CR-4878, (April 1987).

**APPENDIX A: LEAK-RATE EXPERIMENTAL DATA USED
TO VALIDATE THE SQUIRT THERMAL-HYDRAULIC MODEL**

APPENDIX A: LEAK-RATE EXPERIMENTAL DATA USED TO VALIDATE THE SQUIRT THERMAL-HYDRAULIC MODEL

Appendix A contains a listing of the experimental data used as part of the validation studies for the SQUIRT code. This listing is not intended to be a complete representation of the data used for validation. It is merely a reproduction of some of the most appropriate published data for initial validation of the code. The reader is referred to the individual references for detailed information on how a given data set was obtained.

Table A-1 Leak rate data for a long horizontal tube published
by Sozzi and Sutherland (Ref. 49).

Nozzle No: 3

Geometry of the Test Section

Horizontal Test Section

z : Distance along the test section length

$0 \text{ mm} < z < 512.7 \text{ mm}$: Diameter = 12.7 mm

Symbols:

P_o : Stagnation Pressure

H_o : Stagnation Enthalpy (Derived)

X_o : Stagnation Quality (Defined as $(V - V_f(P_o))/V_{fg}(P_o)$)

G_c : Critical Mass Flux

P_t : Critical Pressure

X_t : Critical Quality

Format used (4X, 2F11. 3, F11. 5, F11. 2, 2X, F11. 3, F11. 5)

	P_o (KPa)	H_o (KJ/Kg)	X_o	G_c (Kg/m ² s)	P_t (KPa)	X_t
024	6942. 962	1267. 349	0. 00200	22752. 45	3399. 087	0. 11871
	6894. 700	1265. 023	0. 00200	22752. 45	3378. 403	0. 11811
	6756. 806	1258. 511	0. 00250	22752. 45	3309. 456	0. 11706
	6618. 912	1251. 301	0. 00250	22068. 90	3247. 404	0. 11593
	6481. 018	1244. 556	0. 00300	22068. 90	3171. 562	0. 11510
	6343. 124	1237. 346	0. 00300	22068. 90	3123. 299	0. 11301
	6984. 331	1270. 372	0. 00250	22361. 85	3392. 192	0. 12066
	6942. 962	1268. 279	0. 00250	22361. 85	3371. 508	0. 12024
	6825. 752	1263. 628	0. 00350	22361. 85	3302. 561	0. 12011
	6694. 753	1256. 650	0. 00350	22361. 85	3261. 193	0. 11801
	6570. 649	1251. 068	0. 00400	22361. 85	3199. 141	0. 11711
	6439. 649	1244. 090	0. 00400	22068. 90	3150. 878	0. 11544
	6556. 859	1122. 217	-0. 00250	41013. 00	3281. 877	0. 04391
	6453. 439	1141. 754	-0. 00200	39792. 37	3309. 456	0. 05239
	6343. 124	1158. 267	-0. 00150	31736. 25	3543. 875	0. 05494
	6301. 755	1156. 174	-0. 00150	29539. 12	3571. 454	0. 05346
	6232. 809	1175. 013	-0. 00100	25486. 65	3419. 771	0. 06982
	6060. 441	1196. 876	-0. 00040	21971. 25	3109. 510	0. 09353
	6867. 121	1261. 999	0. 00100	23045. 40	3371. 508	0. 11664
	6825. 752	1260. 604	0. 00150	23045. 40	3337. 035	0. 11702
	6687. 858	1254. 092	0. 00200	23045. 40	3261. 193	0. 11618
	6543. 070	1247. 114	0. 00250	21775. 95	3192. 246	0. 11592
	6405. 176	1239. 904	0. 00250	21775. 95	3143. 983	0. 11383
	6253. 493	1232. 461	0. 00300	21775. 95	3054. 352	0. 11309

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SOURCE: SOZZI AND SUTHERLAND NEDO 13418 JULY 1975

Table A-2 Leak rate data for rectangular slits
published by Collier et al. (Ref. 3).

Run No.	P ₀ MPa	T ₀ C	ΔT C	k μm	COD mm	W(meas) kg/s
1	8.36	265.1	33.0	0.3	1.12	2.03
2	9.27	186.1	119.4	0.3	0.25	0.84
3	9.00	197.2	106.1	0.3	0.25	0.82
4	8.35	180.1	117.9	6.2	0.23	0.90
5	8.77	199.1	102.3	6.2	0.36	0.74
6	8.51	261.6	37.7	6.2	1.09	3.60
7	11.53	251.2	70.4	6.2	1.09	3.93
8	5.81	199.2	74.3	6.2	0.36	0.43
9	5.00	178.4	85.5	6.2	0.23	0.40
10	3.96	200.0	49.8	6.2	0.36	0.46
11	3.26	227.8	10.6	6.2	1.09	1.35
12	10.80	234.2	82.5	6.2	0.23	0.52
13	10.65	249.0	66.6	6.2	0.23	0.48
14	9.91	212.5	97.8	6.2	0.33	0.62
15	8.68	217.9	82.8	6.2	0.33	0.49
16	9.33	234.8	71.1	6.2	0.33	0.52
17	8.80	205.8	95.9	6.2	0.23	0.51
18	8.89	230.1	72.4	6.2	0.23	0.49
19	8.52	236.1	63.4	6.2	0.23	0.46
20	8.65	192.1	108.3	6.2	0.33	0.71
21	8.94	206.6	96.3	6.2	0.33	0.86
22	4.72	195.3	65.1	6.2	1.09	2.16
23	8.90	206.0	96.5	10.2	0.20	0.39
24	9.02	218.9	84.6	10.2	0.20	0.40
25	8.92	223.7	78.9	10.2	0.20	0.39
26	9.46	267.8	39.1	5.2	1.09	3.14
27	10.31	256.1	57.1	6.2	1.09	3.03

Table A-3 Leak rate data for rectangular slits.

$P_o,$ (MPa)	$T_o,$ °C	$\Delta T,$ (°C)	$2a,$ (mm)	COD, (mm)	ACO, (mm ²)	G_c (Measured) (kg/m ² /s)	W (Measured) (kg/s)
6.8	283.8	0	10	0.5	5.0	6.0×10^4	0.30
6.8	283.8	0	10	0.1	1.0	4.0×10^4	0.04
6.8	283.8	0	10	0.2	2.0	3.0×10^4	0.06
6.8	283.8	0	10	0.2	2.0	2.7×10^4	0.054
6.8	283.8	0	10	0.05	0.5	3.2×10^4	0.016

These data were extracted from leak-rate plots published by Yano et al. (Ref. 51) and, therefore, are subject to round-off errors. Channel length = 4, 15, or 36 mm; roughness = 0.0002 mm.

Note: The data points plotted on Figure 4.4 are partly from the above and partly from Ref. 50.

Table A-4 Leak rate data for rectangular slits.

Run #	P ₀ (MPa)	T ₀ (°C)	2a (mm)	COD (mm)	G _c (Measured) (kg/m ² · s)
9	6.817	272.5	20.98	.127	1.677 x 10 ⁴
11	6.939	219.4	20.31	.254	4.877 x 10 ⁴
15	2.734	226.9	20.24	.127	7.210 x 10 ³
17	7.070	221.3	20.24	.127	3.491 x 10 ⁴
19	7.088	224.6	20.33	.127	3.818 x 10 ⁴
20	7.098	224.5	20.33	.127	3.912 x 10 ⁴
21	6.945	270.1	20.33	.127	3.746 x 10 ³
22	7.050	284.2	14.83	.127	1.883 x 10 ⁴
23	7.108	223.8	20.46	.254	4.287 x 10 ⁴
24	7.049	257.5	20.45	.254	3.609 x 10 ⁴
25	7.050	268.8	20.35	.254	3.097 x 10 ⁴
26	7.003	282.7	20.35	.254	2.007 x 10 ⁴
27	7.071	257.2	20.45	.381	4.416 x 10 ⁴
28	7.066	271.2	20.45	.381	3.338 x 10 ⁴
29	7.079	281.1	20.45	.381	2.436 x 10 ⁴
30	7.092	224.0	20.45	.381	5.776 x 10 ⁴
32	9.676	248.5	20.65	.1048	1.932 x 10 ⁴
33	7.076	280.2	20.65	.1048	9.154 x 10 ³
34	7.153	270.3	20.65	.1048	1.080 x 10 ⁴
36	9.557	244.6	20.29	.2292	4.390 x 10 ⁴
37	9.618	280.9	20.29	.2292	3.198 x 10 ⁴
38	9.679	287.0	20.29	.2292	2.699 x 10 ⁴
39	11.60	266.9	20.29	.2292	4.396 x 10 ⁴
41	9.585	246.4	20.40	.3600	5.756 x 10 ⁴
42	11.57	264.5	20.4	.3600	5.772 x 10 ⁴

Table A-4. (Continued)

Run #	P ₀ (MPa)	T ₀ (°C)	2a (mm)	COD (mm)	G _C (Measured) (kg/m ² · s)
43	9.582	278.2	20.4	.3600	4.369 x 10 ⁴
44	9.629	294.3	20.4	.3600	3.265 x 10 ⁴
45	11.604	292.4	20.4	.3600	4.446 x 10 ⁴
47	11.632	263.3	20.42	.0797	2.897 x 10 ⁴
48	9.655	278.1	20.42	.0797	1.882 x 10 ⁴
49	9.671	294.2	20.42	.0797	1.539 x 10 ⁴
50	7.205	224.2	20.42	.0797	1.927 x 10 ⁴
51	7.041	226.1	20.50	.2112	4.053 x 10 ⁴
52	11.708	263.3	20.50	.2112	4.849 x 10 ⁴
54	11.692	292.9	20.50	.2112	3.809 x 10 ⁴
55	9.636	304.5	20.50	.2112	2.205 x 10 ⁴
57	4.221	225.7	19.23	.381	3.280 x 10 ⁴
58	4.186	239.2	19.23	.381	2.502 x 10 ⁴
59	4.270	195.1	19.23	.381	4.095 x 10 ⁴
60	4.150	249.4	19.23	.381	1.460 x 10 ⁴
61	4.217	192.7	19.43	.127	2.216 x 10 ⁴
63	4.232	238.8	19.43	.127	1.293 x 10 ⁴
64	4.242	223.4	19.43	.127	1.663 x 10 ⁴
65	4.325	249.4	19.43	.127	1.229 x 10 ⁴
66	12.105	295.3	19.43	.127	3.109 x 10 ⁴

Table A-4. (Continued)

Run #	P _O (MPa)	T _O (°C)	2a (mm)	COD (mm)	G _C (Measured) (kg/m ² · s)
67	7.203	257.9	19.43	.127	2.071 × 10 ⁴
68	4.279	195.1	19.46	.127	3.479 × 10 ⁴
69	4.280	239.6	19.46	.235	2.138 × 10 ⁴
70	4.318	224.1	19.46	.235	2.824 × 10 ⁴
71	4.270	249.4	19.46	.235	1.685 × 10 ⁴
72	15.588	291.4	19.46	.235	4.985 × 10 ⁴
74	15.369	289.8	19.30	.381	6.955 × 10 ⁴
75	9.546	311.9	19.30	.381	2.624 × 10 ⁴
76	11.666	310.8	19.30	.381	3.859 × 10 ⁴
78	15.435	319.1	19.30	.381	5.139 × 10 ⁴
79	11.686	323.4	19.30	.381	2.889 × 10 ⁴
80	11.787	310.0	18.67	.127	2.218 × 10 ⁴
81	11.574	323.5	18.67	.127	2.011 × 10 ⁴
82	9.661	308.8	18.67	.127	1.693 × 10 ⁴
84	9.562	252.0	18.67	.127	2.996 × 10 ⁴
85	7.189	273.4	18.67	.127	1.544 × 10 ⁴
86	7.179	283.8	18.67	.127	1.472 × 10 ⁴
87	15.927	290.3	18.67	.127	3.759 × 10 ⁴
88	11.860	265.8	18.67	.127	3.245 × 10 ⁴
89	16.140	320.2	18.67	.127	3.018 × 10 ⁴
92	15.798	334.4	18.36	.254	4.210 × 10 ⁴
93	11.831	310.9	18.36	.254	3.192 × 10 ⁴
94	11.672	320.6	18.36	.254	1.981 × 10 ⁴
95	12.429	266.4	18.36	.254	5.089 × 10 ⁴

Table A-4. (Continued)

Run #	P _O (MPa)	T _O (°C)	2a (mm)	COD (mm)	G _C (Measured) (kg/m ² · s)
96	11.638	296.2	18.36	.254	3.842 × 10 ⁴
97	15.753	334.5	18.36	.254	3.646 × 10 ⁴
98	9.769	251.4	18.36	.254	4.850 × 10 ⁴
99	9.614	308.2	18.36	.254	2.222 × 10 ⁴
100	15.736	345.7	18.36	.254	2.985 × 10 ⁴

Data taken from Amos and Shrock (Ref. 49). Channel depth = 63.5 mm.

Table A-5 Leak rate data for intergranular stress corrosion cracks published by Collier et al. (Ref. 3).

(crack depth = 17.3 mm; roughness = 0.00178 mm)

Run No.	P _o MPa	T _o °C	ΔT °C	2a mm	COD mm	ACD mm ²	W (meas) kg/s
1	7.417	272.8	18.4	3.63	0.074	0.268	1.02 x 10 ⁻³
2	7.417	272.8	18.4	"	"	"	"
3	8.768	267.2	34.2	"	"	"	1.10 x 10 ⁻³
4	9.513	260.6	46.8	"	"	"	"
5	9.513	260.6	46.8	"	"	"	"
6	9.423	267.8	38.8	"	"	"	"
7	9.402	284.4	22.1	0.74	0.0220	0.0162	5.49 x 10 ⁻⁵
8	9.547	281.7	25.9	"	0.0218	0.0161	6.66 x 10 ⁻⁵
9	9.568	278.3	29.4	"	0.0216	0.0159	5.67 x 10 ⁻⁵
10	5.803	273.3	.1	"	0.0208	0.0153	3.08 x 10 ⁻⁵
11	6.072	262.8	13.6	"	0.0201	0.0148	1.34 x 10 ⁻⁵
12	5.969	260.0	15.2	"	0.0199	0.0146	1.09 x 10 ⁻⁴
13	5.955	268.3	6.7	"	0.0205	0.0151	1.51 x 10 ⁻⁴
14	5.969	272.8	2.4	"	0.0208	0.0153	1.01 x 10 ⁻⁴
15	3.624	251.7	-7.1	"	0.0190	0.0140	9.30 x 10 ⁻⁵
16	3.480	241.1	1.1	"	0.0183	0.0135	1.05 x 10 ⁻⁴
17	5.782	278.9	-5.7	"	0.0212	0.0156	1.01 x 10 ⁻⁴
18	7.182	260.0	27.6	"	--	--	6.21 x 10 ⁻⁵
19	7.410	273.9	15.8	9.53	0.108	1.026	3.04 x 10 ⁻²
20	7.410	282.2	7.4	"	"	"	2.90 x 10 ⁻²
21	9.106	280.0	24.2	"	"	"	4.85 x 10 ⁻²
22	9.106	273.3	30.8	"	"	"	4.62 x 10 ⁻²
23	9.065	256.7	39.3	"	"	"	4.52 x 10 ⁻²
24	8.989	256.7	36.3	"	"	"	4.44 x 10 ⁻²
25	7.265	256.1	32.2	"	"	"	3.67 x 10 ⁻²
26	7.265	256.7	31.7	"	"	"	3.59 x 10 ⁻²
27	5.672	260.6	11.4	"	"	"	2.51 x 10 ⁻²
28	5.727	267.8	4.8	"	"	"	2.35 x 10 ⁻²
29	5.693	241.7	30.5	"	"	"	3.01 x 10 ⁻²
30	5.693	243.9	28.3	"	"	"	2.98 x 10 ⁻²
31	7.320	242.8	46.1	"	"	"	3.96 x 10 ⁻²
32	7.320	238.3	46.6	"	"	"	3.94 x 10 ⁻²
33	8.913	241.7	60.9	"	"	"	4.51 x 10 ⁻²
34	8.913	235.0	67.6	"	"	"	4.73 x 10 ⁻²
35	7.230	226.1	61.9	"	"	"	3.84 x 10 ⁻²
36	7.230	222.8	65.2	"	"	"	3.92 x 10 ⁻²
37	5.693	220.0	52.2	"	"	"	3.84 x 10 ⁻²
38	5.541	223.2	47.1	"	"	"	2.97 x 10 ⁻²
39	4.038	230.6	20.3	"	"	"	2.62 x 10 ⁻²
40	4.038	232.8	18.1	"	"	"	1.57 x 10 ⁻²

Table A-5 (Continued).

Run No.	P _o MPa	T _o C	ΔT C	2a mm	COD mm	ACD mm ²	W (meas) kg/s
41	7.410	278.9	10.8	1.59	0.0535	0.0849	1.81 x 10 ⁻³
42	8.161	277.2	19.2	"	0.0534	0.0848	1.98 x 10 ⁻³
43	8.823	276.7	25.2	"	0.0535	0.0850	2.19 x 10 ⁻³
44	9.478	274.4	32.6	"	0.0533	0.0847	2.37 x 10 ⁻³
45	7.451	278.3	11.9	"	0.0534	0.0848	3.62 x 10 ⁻⁴
46	8.168	275.0	21.4	"	0.0530	0.0842	5.26 x 10 ⁻⁴
47	8.858	275.0	27.2	"	0.0533	0.0845	3.91 x 10 ⁻³
48	9.533	272.2	35.3	"	0.0530	0.0841	4.37 x 10 ⁻³
49	7.451	276.1	13.9	"	0.0530	0.0841	3.22 x 10 ⁻³
50	8.168	274.4	20.3	"	0.0529	0.0840	3.59 x 10 ⁻³
51	5.314	258.3	9.4	"	0.0490	0.0778	2.51 x 10 ⁻³
52	7.024	253.9	32.1	"	0.0487	0.0773	3.54 x 10 ⁻³
53	8.747	252.8	48.5	"	0.0491	0.0779	4.11 x 10 ⁻³
54	5.314	256.7	11.1	"	0.0487	0.0773	2.20 x 10 ⁻³
55	7.003	254.4	31.4	"	0.0488	0.0775	2.61 x 10 ⁻³
56	8.747	253.3	47.9	"	0.0492	0.0781	3.17 x 10 ⁻³
57	3.514	233.9	8.9	"	0.0438	0.0696	7.57 x 10 ⁻⁴
58	5.272	232.8	34.5	"	0.0442	0.0711	9.03 x 10 ⁻⁴
59	7.024	231.1	54.9	"	0.0445	0.0706	1.50 x 10 ⁻³
60	8.747	228.9	72.4	"	0.0446	0.0708	1.85 x 10 ⁻³
61	8.747	231.7	69.6	"	0.0451	0.0717	1.75 x 10 ⁻³
62	7.134	228.3	58.0	"	0.0440	0.0698	1.76 x 10 ⁻³
63	5.307	225.0	42.7	"	0.0428	0.0679	1.53 x 10 ⁻³
64	3.549	222.8	20.6	"	0.0415	0.0663	1.33 x 10 ⁻³
65	3.562	235.0	8.6	"	0.0441	0.0700	1.84 x 10 ⁻³
66	5.286	232.2	35.3	"	0.0441	0.0700	2.32 x 10 ⁻³
67	6.996	230.0	55.8	"	0.0443	0.0703	2.69 x 10 ⁻³
68	8.747	228.3	71.8	"	0.0445	0.0707	2.69 x 10 ⁻³
69	8.727	241.7	59.4	27.89	0.235	6.547	1.51 x 10 ⁻¹
70	7.037	236.8	49.5	"	0.227	6.338	1.39 x 10 ⁻¹
71	6.962	236.7	48.8	"	0.227	6.334	1.57 x 10 ⁻¹
72	8.637	241.1	59.3	"	0.234	6.528	1.75 x 10 ⁻¹
73	5.210	252.8	13.7	"	0.178	4.952	1.41 x 10 ⁻¹
74	6.962	253.9	31.6	"	0.243	6.787	1.69 x 10 ⁻¹
75	8.706	254.4	46.5	"	0.247	6.882	1.91 x 10 ⁻¹
76	8.699	250.6	50.3	"	0.243	6.779	2.00 x 10 ⁻¹
77	6.962	248.3	37.1	"	0.238	6.641	1.78 x 10 ⁻¹
78	5.293	246.1	21.4	"	0.173	4.828	1.51 x 10 ⁻¹
79	4.038	240.6	10.3	"	0.144	4.024	1.28 x 10 ⁻¹
80	5.190	236.1	30.2	"	0.166	4.633	1.55 x 10 ⁻¹
81	6.872	237.2	47.3	"	0.228	6.345	1.76 x 10 ⁻¹
82	8.678	234.4	66.3	"	0.228	6.355	1.99 x 10 ⁻¹

Table A-6 Leak rates for a fatigue crack in a girth weld (data obtained as part of the research program to evaluate leak rate estimation models).

(crack length = 1.31 mm; crack depth = 8.28 mm)

Pressure (MPa)	Temperature (°K)	Bending Moment (N-m)	Inside COD (μm)	Outside COD (μm)	Measured Leakage (kg/s)
9.04	547	0	106	131	.0339
9.50	548	0	106	132	.0356
11.5	551	0	110	135	.0444
12.5	552	0	112	137	.0467
13.5	553	0	114	139	.0525
14.3	553	0	115	141	.0558
7.92	546	0	103	129	.0300
10.4	549	0	108	133	.0374
12.7	552	0	112	138	.0454
14.9	554	0	116	142	.0635
8.93	551	13500	128	153	.0448
10.5	553	13700	131	156	.0508
11.3	553	13800	132	158	.0540
14.3	554	13900	138	164	.0629
9.91	552	20000	140	166	.0504
12.4	554	20100	145	170	.0592
12.7	554	20100	145	171	.0604
14.4	555	20100	149	174	.0651
7.84	551	25700	146	171	.0447
10.3	553	25700	150	176	.0562
12.4	554	25900	154	180	.0613
13.6	555	25500	156	181	.0660

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