THE EFFECT OF DISCONTINUITIES ON THE FATIGUE BEHAVIOR OF TRANSVERSE BUTT WELDS IN STEEL

By
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and
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The primary objectives of this study were to identify and evaluate the variables that influence the fatigue behavior of transverse butt welded steel members containing various weld discontinuities, and to formulate fatigue based weld quality rating criteria. Mathematical models for evaluating the fatigue strength of welds containing discontinuities have been developed from empirical fatigue relationships. Empirical relationships for welds with nonplanar discontinuities are used to relate fatigue strength zones to known discontinuity severities; welds containing planar discontinuities are modeled by partitioning the fatigue life into initiation and propagation phases for given discontinuity severities.

An experimental program was conducted to examine the accuracy of the mathematical models. Fatigue tests were conducted on notched mild steel plain plate specimens, welded mild steel and quenched and tempered steel specimens containing artificial discontinuities, and quenched and tempered transverse butt-welded steel specimens containing slag inclusions. Strain relaxation tests were conducted also to determine the magnitude and distribution of residual stresses in the quenched and tempered steel welds.

A simple-to-use method is suggested to relate weld fabrication quality to the reduction in mean fatigue strength of a sound weldment. The recommendations are formulated from fatigue strength rating curves which relate predicted fatigue strengths for welds containing discontinuities to mean fatigue strengths of sound welds. Use of the suggested weld fabrication quality levels provides a simple means of assessing the effect of different types of discontinuities on the weldment fatigue strength.

**Key Words**

Fatigue, Butt welds, Discontinuities, Crack initiation, Crack propagation, Slag, Porosity, Undercut, Lack of Penetration, Residual stresses, Rating criteria, Design

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The Effect of Discontinuities on
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Butt Welds in Steel

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The investigations discussed herein were a part of the structural research program of the Department of Civil Engineering and of the Engineering Experiment Station in the College of Engineering at the University of Illinois at Urbana-Champaign. The research has been conducted by Mark D. Bowman, Research Assistant in Civil Engineering under the direction of W. H. Munse, Professor of Civil Engineering. Appreciation is extended to Mr. John R. Williams who prepared the weldments, to Mr. Glen H. Lafenhagen who assisted in the fatigue testing, to Mrs. Marianne C. Day who typed the manuscript for this report, and to the various laboratory mechanics and technicians who assisted with the preparation of test specimens.
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Finally, it should be noted that the opinions, findings and conclusions expressed herein are those of the authors and not necessarily those of the Illinois Division of Highways, of the Federal Highway Administration, of the U.S. Navy, or of the University of Illinois.
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LIST OF SYMBOLS

\( a \) = Half crack width
\( a_{avg} \) = Average half crack width
\( a_i, a_f \) = Initial and final half crack width
\( a_{lop}, a_{lof} \) = Lack of penetration, lack of fusion half depth
\( a_u \) = Undercut depth
\( a^* \) = Material constant
\( b \) = Fatigue strength exponent
\( C \) = Crack propagation material constant
\( c \) = Fatigue ductility exponent
\( D_i \) = Total damage for \( i \)-th strain reversal
\( D_{i,e} \) = Elastic damage for \( i \)-th strain reversal
\( D_{i,p} \) = Plastic damage for \( i \)-th strain reversal
\( D_{i,m} \) = Mean stress damage for \( i \)-th strain reversal
\( d_1 \) = Distance from midwidth of a crack to closest edge
\( d_e \) = Half width of major axis of elliptical crack
\( d\mu \) = Infinitesimal layer of material
\( dS_1 \) = Change in surface 1 stress
\( E \) = Modulus of elasticity
\( E_i \) = Spring stiffness for \( i \)-th element
\( E^*_i \) = Slope of \( i \)-th segment of stress-strain curve
\( E_k \) = Elliptical integral of the second kind
\( e, \Delta e \) = Nominal strain, range
\( e_1^{0}, e_2^{0} \) = Surface 1 or 2 strain resulting from cutting a block in two
\( e_1', e_t' \) = Relaxed strain on longitudinal or transverse block resulting from removing a block from a plate
Relaxed strain on longitudinal or transverse block resulting from splitting a block and removing layers of material

\( F(a) \) = Stress-intensity correction factor

\( F_D \) = Reduction coefficient

\( F_E \) = Correction factor for elliptical crack shape

\( F_G \) = Correction factor for nonuniform stress

\( F_S \) = Correction factor for free surface

\( F_W \) = Correction factor for finite width

\( F(|\sigma|) \) = Mean stress relaxing function

\( f^* \) = Secondary correction factor

\( G \) = Shear modulus

\( h_i \) = Distance from crack midwidth to start of \( i \)-th interval

\( K, \Delta K \) = Stress-intensity factor, range

\( K_C \) = Critical stress-intensity factor

\( K_f, K_{f_{\text{max}}} \) = Fatigue notch factor, maximum value

\( K_{Ic}, K_{Id} \) = Limiting value of critical, dynamic stress-intensity factor

\( K_{I}, K_{II}, K_{III} \) = Stress-intensity factor for Mode I, II, and III deformation

\( K_{\text{max}}, K_{\text{min}} \) = Maximum, minimum stress-intensity factor

\( K_{OL} \) = Overload stress intensity factor

\( K_t \) = Theoretical stress intensity factor

\( K_c, K_{c_c} \) = Stress, strain, concentration factor

\( K^* \) = Monotonic strength coefficient

\( K' \) = Cyclic strength coefficient

\( L \) = Effective half crack width
M = Relaxing constant
m = Crack growth rate exponent
\(2N_F\) = Reversals for failure of smooth specimen
\(N_I\) = Fatigue crack initiation life
\(N_P\) = Fatigue crack propagation life
\(N_T\) = Total fatigue life
\(n^*\) = Monotonic strain hardening exponent
\(n'\) = Cyclic strain hardening exponent
R = Stress ratio
\(R_N\) = Half notch width
r = Radial distance from crack tip
\(r_N\) = Notch root radius
\(S,\Delta S\) = Nominal stress, range
\(S_r\) = Residual stress
\(S_{YS, SYBM}\) = Yield strength, yield of base metal
\(S_{x,y,z}\) = Stress in x, y, or z direction
\(S_{\alpha}\) = Relaxed stress at depth \(\alpha\)
\(S_{\alpha', \alpha', \alpha', \alpha'}\) = Relaxed stress at depth \(\alpha\) resulting from removing block, splitting block in two, and removing layers of material
\(S_{\alpha', \alpha', \alpha'}\) = Relaxed stress on face of longitudinal or transverse block resulting from removing a block
\(S_{\alpha', \alpha', \alpha'}\) = Relaxed stress on face of longitudinal or transverse block resulting from splitting a block in two and removing layers of material
\( S_1, S_2, S_\alpha \) = Relaxed stress on surface 1 or 2 or depth \( \alpha \) resulting from splitting a block in two

\( u, v, w \) = Displacements in the \( x, y \) and \( z \) directions

\( W \) = Plate width

\( \alpha \) = Fraction of plate thickness

\( \beta \) = Fraction of unrelieved stress

\( \gamma \) = Angle between crack and applied stress

\( \Delta a \) = Crack increment

\( \Delta \delta, \Delta \delta_T \) = Crack opening displacement range, transition value

\( \Delta \varepsilon_e, \Delta \varepsilon_p \) = Elastic and plastic strain amplitude

\( \Delta K_{th} \) = Threshold stress-intensity factor range

\( \delta \) = Ratio of crack offset to half plate width

\( \varepsilon, \Delta \varepsilon \) = True strain, range

\( \varepsilon_f' \) = Fatigue ductility coefficient

\( \theta \) = Polar coordinate from crack tip

\( \lambda \) = Crack size to edge distance ratio

\( \nu \) = Poisson's ratio

\( \sigma, \Delta \sigma \) = True stress, range

\( \sigma_a \) = Stress amplitude

\( \sigma_f' \) = Fatigue strength coefficient

\( \sigma_0 \) = Mean stress

\( \tau_{xy} \) = Shear stress in the \( x-y \) plane

\( \psi \) = Ratio of notch offset to plate width
1.1 Historical Background of Fatigue

It has long been known that metallic components subjected to repeated loads can fail with little deformation or warning of impending danger. However, the same components could withstand a much greater loading if the load was applied steadily and maintained constant (1). It is the repetition of the loading that gives rise to the reduced strength, and the result of this phenomenon has been termed "fatigue".

The first fatigue tests were performed in Germany in 1829 by Albert (2) on mine-hoist chains, which were repeatedly proof loaded in tension before being put into service. However, it was Wöhler (3), chief locomotive engineer for the Royal Lower Silesian Railways, who first conducted fatigue tests where the magnitude of the applied loading was carefully monitored. From 1852 to 1869 he conducted numerous fatigue experiments to determine the cause of axle failures. He is credited also with building the first testing machine for repeated loading, known as the rotating bending machine, and with advancing two
fundamental laws of fatigue:

1. Iron and steel may fracture under a unit stress not merely less than the static rupture stress, but also less than the elastic limit, if the stress is repeated a sufficient number of times.

2. However many times the stress-cycle is repeated, rupture will not take place if the range of stress between the maximum and minimum stress is less than a certain limiting value." (1)

A thorough understanding of the fatigue phenomenon, first sought nearly a century and a quarter ago, is still being actively pursued today. Although many advances have been made towards understanding the mechanisms of fatigue, a unified and usable solution has not yet been achieved, and will only be attained through continued research.

1.2 Historical Background of Welding

Welding is a method used to join two pieces of metal. Lesnewich (4) defines welding as, "a metal-joining process wherein coalescence is produced by heating to suitable temperatures, with or without the application of pressure, and with or without the use of filler metal". Welding is not a new process, but most likely dates to the era when man first manufactured wrought iron. Hammer welding, for example, was well known more than a thousand years ago, and was used to fabricate ancient swords and daggers by forge welding strips of low-carbon and high-carbon steel together (5).
The beginning of arc welding as we know it today dates back to 1885. Nikolai Benardos, a Russian Scientist working at the Cabot Laboratory in France, discovered that when an arc was struck between a carbon cathode and a metal anode a small localized casting was formed which could be used to join two pieces of metal together (6). Several additional discoveries were made during the next thirty years including metal electrodes, electrode coatings, gas welding, resistance welding, and thermit welding. However, the increased demand for machinery and equipment during World War I accounted for many of the innovative ideas and advances in welding technology. A notable example is the widespread publicity welding received when used to repair sabotaged cast-iron cylinders of the large reciprocating engines of German ships interned in New York harbor. These ships were quickly put back into service and used to deliver supplies from the U.S. to Europe (7). Since that time welding has developed further and has become an indispensable and economic means of joining two pieces of metal. Welding is widely accepted today and has innumerable applications ranging from home appliances, automobiles, and railroad cars to buildings, bridges, and space craft.

1.3 Weld Strength and Discontinuities

The ultimate strength and hardness of weld metal is generally superior to that of base metal, although weld metal ductility is often less than that of the base metal. The strength of a weldment, however,
in comparison to plain material depends upon the presence of discontinuities and the type of loading.

"Discontinuity" is used to denote an entity which interrupts the continuity of a weldment. Discontinuities can be of two types: external and internal. An external discontinuity pertains to the weld surface; weld bead reinforcement, undercut, and overfill are examples of external discontinuities, Fig. 1.1a. An internal discontinuity occurs in the interior of a weld; examples include porosity, slag inclusions, lack of penetration, and lack of fusion, Fig. 1.1b. The noun "defect" is often incorrectly used to describe discontinuities. The term "weld defect" implies that a weldment is defective and of poor quality, and its use should be limited to cases where a discontinuity reduces the weldment strength below the level for which it was originally intended.

A weldment subjected to a static load can contain limited levels of discontinuities without a reduction in connection strength because the strength of weld metal often overmatches that of the base metal. Green et al. (8) for example, have shown that mild steel welds can contain a uniform porosity severity of seven percent of the cross-sectional area without altering the measured tensile strength, percent tensile and bend elongation in 2-inches (50.8 mm), angle of bend, and impact-energy absorption. A similar, albeit not identical, behavior exists also for other internal discontinuities. In general, the discontinuity severity which is present before a reduction in the measured mechanical properties is obtained depends on several factors such as the
discontinuity type, discontinuity position, and the ratio of weld metal to base metal strengths.

A discontinuity severity that does not affect the strength of a statically loaded member can, however, significantly reduce the fatigue strength as a result of a concentration of stress that occurs at the discontinuity. In a statically loaded member the increased stress is relieved by plastic strain of the material near the discontinuity. However, considerably less plastic deformation occurs in a member subjected to a fluctuating load and, consequently, the range of stress at the discontinuity remains much higher than in the surrounding material. The result is a reduced fatigue strength (9).

Unfortunately, numerous case histories of failures resulting from weld defects have been reported for a wide variety of structures. Thielsch (10) has summarized several failures in pressure vessels and pipes; discontinuities in these structures are particularly subject to distress from high biaxial stress fields that result from welding and internal pressure. Highway bridges, which are subjected to low temperatures, corrosive environments, and random stress fluctuations, are susceptible also to distress from weld defects. Fisher et al. (11,12) have described the propagation of fatigue cracks, discovered in highway bridges located in Connecticut and Minnesota, that initiated from lack of fusion defects at stiffener details. Handel et al. (13) describe the brittle fracture of an Illinois highway bridge that initiated from plug welds containing slag and fusion defects.
It is significant to note that the initiation and propagation of fatigue cracks from weld defects often occurs at service loads which are well below the maximum design loads. It is clear that weld quality standards for members subjected to repeated loads cannot be logically formulated without proper consideration of the reduced fatigue strength as a result of the allowable discontinuity.

1.4 Scope and Outline of the Study

Mathematical models that are used to estimate the fatigue crack initiation and propagation lives of notched members are described. Notches with various shapes and through thickness positions are used to describe weld discontinuities, allowing the fatigue models to be used to examine the effect of weld discontinuities on fatigue behavior.

Experimental fatigue studies using specimens with artificially modelled discontinuities have been conducted to assess the accuracy of the mathematical models. Fatigue studies have been conducted also on weldments containing slag inclusions to assess the effect of actual discontinuities.

The computed fatigue strengths of weldments containing discontinuities are compared to known mean fatigue strengths. From these comparisons weld quality standards are suggested which relate levels of weld quality to a minimum expected fatigue performance.
In Chapter 2, the mathematical models used to estimate the fatigue crack initiation and propagation lives of notched members are described. The model for fatigue crack initiation life consists of a mechanics analysis, a cyclic stress-strain relationship, a strain-life relationship, and a linear damage summation. The model for fatigue crack propagation life consists of a crack growth-rate equation that utilizes the range in stress-intensity and measured material constants. The effect of secondary factors on various aspects of the fatigue models are discussed also.

In Chapter 3, a description is given of the experimental testing program. Details of the materials, the fabrication procedures, and the testing and monitoring equipment are described for all tests conducted during the investigation.

In Chapter 4, results of the fatigue testing program are presented and observations discussed. Comparisons between computed and experimental fatigue lives are examined, and general trends of the tests are indicated.

Chapter 5 describes the use of the mathematical models for evaluating the fatigue behavior of weldments containing planar or nonplanar discontinuities. The computed fatigue lives of weldments containing discontinuities are used to develop reduction factors that adjust the mean fatigue lives of sound weldments. Levels of weld quality based on known reductions in fatigue strength are suggested.
Chapter 6 contains a summary of the investigation and conclusions that were formulated therein.
2.1 Introduction

The fatigue failure process is divided phenomenologically into three periods: (a) crack nucleation, (b) crystallographic (Stage I) crack growth along planes closely aligned with the direction of maximum shear stress, (c) and stable (Stage II) crack growth on planes perpendicular to the applied tensile stress (14). However, the engineering approach for describing the fatigue failure process only considers two separate periods: fatigue crack initiation and fatigue crack propagation. Fatigue crack initiation life, \( N_I \), can be defined as the number of loading cycles necessary to initiate a macroscopic, subcritical fatigue crack and fatigue crack propagation life, \( N_p \), may be defined as the number of loading cycles required to propagate a crack to a critical size associated with failure of the member.

General mathematical models, which are used to calculate the fatigue crack initiation and propagation lives, are discussed in the following two sections of this chapter. Also discussed are factors which affect fatigue behavior and methods which can be used to incorporate these factors into the mathematical models. Subsequent sections consider
The mathematical fatigue models presented in this chapter are used in subsequent chapters to examine experimental fatigue data and formulate weld quality levels.

2.2 Fatigue Crack Initiation

2.2.1 Crack Initiation Models

The fatigue crack initiation life often represents a significant proportion of the total fatigue life of a structure, especially when of high strength material and subjected to low stresses. Therefore, the total fatigue life of a structure cannot be accurately estimated without a method to model the crack initiation phase. One such technique that has been successfully used to calculate the fatigue crack initiation life of notched members is the "critical location approach."

The critical location approach is based primarily on two assumptions. First, it is assumed that the most severe stress raisers (critical locations) in a structure can be identified—these are usually sharp notches, fillets, or sudden changes in geometry. Second,
it is assumed that the cyclic behavior of smooth laboratory specimens can be used to represent the cyclic stress-strain behavior at the notch root. In support of the latter, Leis et al. (15) observed equal low cycle fatigue lives in notched and smooth steel and aluminum specimens when the strains at the notch root were equal to the strains of smooth specimens.

Topper and Morrow (16) have outlined the requirements necessary for an analysis of the fatigue crack initiation life of a notched member:

1. A mechanics analysis to relate the nominal stresses and strains to the local stresses and strains.
2. Knowledge of the cyclic stress-strain properties to determine the material response to a given stress or strain.
3. Knowledge of the fatigue properties of the metal.
4. A cumulative damage procedure which will predict the initiation life based on the accrued damage of all previous load reversals.

Each of these requirements are reviewed separately below.

2.2.1.1 Mechanics Analysis

A mechanics analysis is defined as a method used to relate the remotely applied stress and strain to the local values of stress and
strain at the notch root, Fig. 2.1. Neuber (17) and Stowell (18) each recommended a method for determining local stress-strain history of notches. A comparison of these analyses by Morrow et al. (19) indicates that Neuber's analysis is more convenient and slightly more accurate than Stowell's analysis. Also, the Neuber rule has been shown experimentally by Wetzel (20) to accurately estimate the local strain histories of notched plates fatigue cycled from zero to tension. Gowda et al. (21), however, caution that the Neuber analysis loses accuracy when the nominal strains are large - i.e., more than about two percent. Values of strain of this magnitude seldom occur for normal service loads, and use of the Neuber analysis is reasonably justified for most situations.

Neuber's equation (17) for elastic or nonlinear elastic loading may be written as

\[ K_t = (K_0 K_{e})^{0.5} \]  \hspace{2cm} (2.1)

where

- \( K_t \) = theoretical elastic stress concentration factor,
- \( K_0 \) = \( \sigma/S \), stress concentration factor,
- \( K_e \) = \( \varepsilon/e \), strain concentration factor,
- \( \sigma, \varepsilon \) = local stress and strain at the notch root,
- \( S, e \) = remote stress and strain.

Topper et al. (22) have shown that Neuber's equation is applicable for fluctuating loads if \( K_f \) is substituted for \( K_t \) and the ranges of stress and strain are used.
\[ K_f = \left( \frac{\Delta \sigma \cdot \Delta \varepsilon}{\Delta S \cdot \Delta e} \right)^{0.5} \]  \hspace{1cm} (2.2)

where \( K_f \) = fatigue notch factor,
\( \Delta \sigma, \Delta \varepsilon \) = local stress and strain ranges at the notch root,
\( \Delta S, \Delta e \) = remote stress and strain ranges.

When the remote loading is elastic, which is typical of most service conditions, Eq. 2.2 can be written in the following manner:

\[ \frac{(K_f \Delta S)^2}{E} = \Delta \sigma \Delta \varepsilon \]  \hspace{1cm} (2.3)

where \( E \) = modulus of elasticity.

The fatigue notch factor, \( K_f \), is defined as a constant for a given material and geometry. The value of \( K_f \) is usually determined by comparing long-life fatigue data of notched and unnotched specimens; \( K_f \) is always less than or equal to \( K_t \). Several empirical equations for determining \( K_f \) factors have been proposed (23,24,25). Of these, Peterson's equation (24) is widely accepted because of its accuracy and simplicity, and may be written as:

\[ K_f = 1 + \frac{K_t - 1}{1 + a^* / r_N} \]  \hspace{1cm} (2.4)

where \( a^* \) = material constant,
\( r_N \) = notch root radius.
The material constant $a^*$ depends on the ductility and ultimate tensile strength of a metal. Peterson (24) proposed the following relationship for heat treated steels:

$$a^* = 0.001 \left( \frac{300}{S_u} \right)^{1.8} \text{ inches}$$

$$= 0.0254 \left( \frac{2070}{S_u} \right)^{1.8} \text{ mm.} \quad (2.5)$$

where $S_u =$ ultimate strength, ksi (MPa).

By combining Eqs. 2.3, 2.4, and 2.5 the notch root stresses and strains of a steel member can be determined for a given loading spectrum once the theoretical stress concentration factor has been determined.

2.2.1.2 Cyclic Stress-Strain Behavior

For most metals the cyclic stress-strain curve is significantly different than the monotonic stress-strain curve. Osman (26), for example, using miniature tensile coupons taken from the notch region of cyclicly loaded mild steel specimens, observed a much lower departure from elastic behavior, a disappearance of the sharp yield point, and a decrease in the elastic modulus. Also, cyclic stress-strain curves, which are determined from low-cycle strain-controlled fatigue tests of smooth specimens, clearly demonstrate the differences between monotonic and cyclic material response. Figure 2.2, for example, shows a typical comparison between the monotonic and cyclic stress-strain curves. For
the material shown in this sketch, cyclic softening will occur at strain ranges for which the stress given by the monotonic curve is greater than that of the cyclic curve; cyclic hardening occurs for strain ranges greater than that given by the intersection of the two curves.

Landgraf (27) defines the cyclic stress-strain curve as, "a curve obtained by connecting the tips of stable hysteresis loops for companion specimens tested at different (completely reversed) strain amplitudes." Morrow (28) has suggested an equation that can be used to describe such a curve.

\[
\frac{\Delta e}{2} = \frac{\Delta \sigma}{2E} + \left( \frac{\Delta \sigma}{2K'} \right)^{1/n'}
\]  

(2.6)

where

- \( K' \) = cyclic strength coefficient,
- \( n' \) = cyclic strain hardening exponent.

The total strain amplitude in Eq. 2.6 represents the sum of the elastic and plastic strain amplitude components. (A curve similar in form to Eq. 2.6 can be used to represent the monotonic stress-strain curve if the monotonic strength coefficient, \( K' \), is substituted for \( K' \), the monotonic strain hardening exponent, \( n' \), is substituted for \( n' \), and the total stress and strain are used instead of the stress and strain amplitudes.)

For materials which behave in a manner consistent with Massing's hypothesis, the cyclic stress-strain curve may be used to describe the hysteresis loop shape. (Massing's hypothesis states that either branch
of a hysteresis loop is geometrically similar to the monotonic stress-strain curve with a scale factor of two.) Halford and Morrow (28,29) have shown that the cyclic stress-strain curve, for metals which cyclically harden or soften prior to stabilization, when amplified by a factor of two accurately describes the hysteresis loop shape.

Other factors which can affect the cyclic stress-strain response include memory, cyclic softening and hardening, and mean stress relaxation (30). Memory effects involve the influence of previous residual strain history on the stress-strain path, Fig. 2.3a. After reversing the load at a and reloading, the stress-strain curve follows path bcd, not path bcd', as the hysteresis curve would indicate. Cyclic softening and hardening can be described as the decrease or increase in stress amplitude for a constant strain level, Fig. 2.3b. Mean stress relaxation is a cycle dependent decrease in the absolute value of mean stress under constant strain conditions, Fig. 2.3c. Strain amplitude is the most important variable in mean stress relaxation, but prior loading history, mean strain, and initial mean stress may be influential also (31).

2.2.1.3 Fatigue Life Properties

Fatigue life properties are used to relate strain amplitude to the fatigue life of smooth specimens. Total strain amplitude is separated into two portions: the elastic strain amplitude (fatigue strength
properties) and the plastic strain amplitude (fatigue ductility properties).

The elastic portion of the strain amplitude is obtained from Basquin's (32) linear relationship between the logarithm of the stress amplitude and the logarithm of the fatigue life.

\[ \sigma_a = \sigma_f' (2N_f)^b \]  

(2.7)

where

- \( \sigma_a \) = stress amplitude,
- \( \sigma_f' \) = fatigue strength coefficient,
- \( b \) = fatigue strength exponent,
- \( 2N_f \) = number of stress reversals for failure of smooth specimen.

The term "reversal" indicates that the direction of the control variable - load, strain, or stroke - in the fatigue test has been reversed. One cycle equals two reversals in constant amplitude tests. The fatigue strength coefficient is the intercept of the stress amplitude at one reversal, and the fatigue strength exponent is the slope of the stress-life curve. Elastic strain amplitude, \( \Delta \varepsilon_e/2 \), can be obtained directly from the stress amplitude.

\[ \Delta \varepsilon_e/2 = (\sigma_f'/E)(2N_f)^b \]  

(2.8)

Mean stress effects can be treated as an increase or decrease in the
fatigue strength coefficient, depending on whether the mean stress is compressive or tensile (33).

\[ \sigma_a = (\sigma_f' - \sigma_o)(2N_f)^b \]  \hspace{1cm} (2.9)

The sign of the mean stress, \( \sigma_o \), in Eq. 2.9 is positive for a tensile mean stress and negative for a compressive mean stress.

The plastic strain amplitude is given by a relationship similar in form to Eq. 2.8.

\[ \Delta \varepsilon_{p/2} = \varepsilon_f' (2N_f)^c \]  \hspace{1cm} (2.10)

where \( \Delta \varepsilon_{p/2} \) = plastic strain amplitude,
\( \varepsilon_f' \) = fatigue ductility coefficient,
\( c \) = fatigue ductility exponent.

The fatigue ductility coefficient is the intercept of the strain amplitude at one reversal, and the fatigue ductility exponent is the slope of the plastic strain-life curve.

The relation between the total strain amplitude, elastic plus plastic, and the fatigue life is obtained by adding Eqs. 2.8 and 2.10.

\[ \frac{\Delta \varepsilon}{2} = \frac{\sigma_f'}{E} (2N_f)^b + \varepsilon_f'(2N_f)^c \]  \hspace{1cm} (2.11)

The general shape of the curve generated from Eq. 2.11 is shown in Fig. 2.4.
The variables that define the strain amplitude vs. fatigue life curve for a given material can be either estimated from empirical relationships (33) or more precisely determined using strain-controlled low cycle fatigue tests. About seven to ten such tests are usually required to describe the shape of the curve (34).

2.2.1.4 Cumulative Damage Procedure

The fatigue damage for a particular reversal may be defined as the inverse of the fatigue life, where the fatigue life represents the life required for failure of a smooth specimen subjected to a constant strain amplitude equivalent to the strain amplitude applied during the loading reversal. The damage caused by each loading reversal is accumulated until a limiting damage criterion is satisfied. The total damage for each loading reversal consists of the damage that results from the elastic stress, plastic strain, and mean stress (35).

The damage for the i-th reversal in terms of the elastic strain amplitude, $D_{i,e}$, is obtained from Eq. 2.7.

$$D_{i,e} = \left( \frac{\Delta \sigma / 2}{\sigma_f} \right)^{-1/b} \tag{2.12}$$

The damage for the i-th reversal in terms of the plastic strain amplitude, $D_{i,p}$, is obtained from Eqs. 2.6 and 2.10.
The damage for the i-th reversal resulting from mean stress, \( D_{i,m} \), has been shown by Martin (35) to be
\[
D_{i,m} = (\varepsilon'_f)^{1/c} \left[ \left( \frac{\Delta \sigma}{K'} \right) - \frac{1}{n_c} \right] \left[ 1 - \frac{\sigma_0}{\sigma_f} \right]^\frac{1}{n_c} - 1
\]

(2.14)

The total damage for the i-th reversal, \( D_i \), is the sum of Eqs. 2.12, 2.13, and 2.14.
\[
D_i = D_{i,e} + D_{i,p} + D_{i,m}
\]

(2.15)

Using the Miner (36) linear cumulative damage rule, the number of reversals for fatigue crack initiation is obtained when the summation of the individual damage components on a reversal by reversal basis equals unity.
\[
\sum_i \left( \frac{1}{2N_f} \right)_i (2N_i) = \sum_i (D_i)(2N_i) = 1.0
\]

(2.16)

The linear damage rule has been criticized for being too simple and
without extensive experimental verification (37,38). Schutz et al. (38) believe that the local stress-strain approach for estimating initiation lives can be improved significantly by use of a "correct" damage summation rule. Nevertheless, Miner's rule is still widely accepted because of its simplicity and ease of application, and is used for this investigation.

2.2.1.5 Combination of the Requirements

A method for combining the requirements discussed above to determine the fatigue crack initiation life is shown schematically in Fig. 2.5. As shown in the figure, the first task is to determine the critical notch locations for the member being examined; the fatigue life for each notch is then computed separately with the initiation life equal to the minimum computed value. The theoretical stress concentration factor defines the fatigue notch factor, which is used in the mechanics analysis.

The fatigue notch factor for a given notch configuration passes through a maximum value at a particular (critical) notch root radius. The maximum value of $K_f$ will give the smallest fatigue crack initiation life, and should be used since the critical notch root radius will most likely exist at some location along the notch length as a result of
surface roughness or notch unevenness. The value of the critical notch root radius is determined by equating the first derivative of the expression for $K_f$ with respect to the notch root radius to zero, and solving for the resulting notch root radius. The maximum value of $K_f$ occurs at this critical notch root radius.

For each reversal, the remote loading is input to the mechanics analysis, which is combined with the cyclic stress-strain curve, to determine the values of local stress and strain. The fatigue properties and the local strain amplitude are then combined to determine the damage for that reversal, which is added to the accumulated damage. Fatigue crack initiation is assumed to have occurred when the damage criterion is satisfied.

2.2.2 Methods for Applying the Initiation Model

The method outlined above for computing the fatigue crack initiation life can be adapted for computer solution. The local stresses and strains for each reversal are determined according to Neuber control using the computer to trace the cyclic hysteresis loop. Also, the fatigue damage is computed for each reversal and added to the total accumulative damage. The time involved in performing the same computations by hand would be prohibitive.

Martin (35) developed a rheological model for the cyclic stress-
strain curve using a number of spring and slider segments, Fig. 2.6a. The factor \( f(\sigma_0) \) modifies the stiffness of the springs in the model to account for cyclic relaxation of mean stress. Each individual element acts in an elasto-plastic manner; for example, the \( i \)-th element acts in an elastic fashion with stiffness \( E_i \) until fully plastic behavior occurs at a yield stress \( \sigma_{iy} \), Fig. 2.6b. The slope of the stress-strain curve for the \( i \)-th element of the rheological model, as shown in Fig. 2.6c, is given by

\[
\frac{1}{E_i} = \frac{1}{E_1} + \left( \frac{1}{E_2} + \frac{1}{E_3} + \ldots + \frac{1}{E_i} \right) / f(\sigma) \tag{2.17}
\]

where \( E_i \) = stiffness of the \( i \)-th spring element,
\( E_i^\ast \) = slope of the \( i \)-th segment of the stress-strain curve,

\[
f(\sigma) = 1 - \frac{\sigma}{M} ,
\]

\( M \) = relaxing constant.

The value of the relaxing constant, \( M \), in the mean stress relaxing function controls the rate of relaxation. A high value of \( M \) results in a slow rate of relaxation. Unless \( M \) is quite small, only small changes of the element moduli occur for various values of \( |\sigma| \).

The computer program used in this investigation to estimate fatigue crack initiation lives is based on a program developed by Plummer (39) and later revised by Mattos (40). Plummer developed a computer model to simulate the cyclic response of A-36 mild steel subjected to a spectrum loading. The program was revised by Mattos (40) to determine fatigue
crack initiation lives by adding Neuber control to determine the local stresses and strains, and a damage summation. The Plummer-Mattos computer program is adequate for determining the initiation lives of mild steel notched plates, but has been revised for modeling the initiation lives of notched plates in welded members. The weld metal has been assumed to behave in a manner consistent with Massing's hypothesis, allowing use of the doubled cyclic stress-strain curve to model the hysteresis loop shape.

2.3 Fatigue Crack Propagation Life

2.3.1 Crack Propagation Models

After a crack has initiated the remainder of the fatigue life is spent in propagation of the crack to a critical or limiting size before fracture. In the late 1950's and early 1960's a number of equations for describing the rate of stable fatigue crack growth were proposed (41,42,43). Although the form of the equations differed, the two primary variables in most of the crack growth models are the applied stress and crack size. In 1963 Paris et al. (44) proposed a crack growth rate model based on the stress-intensity factor range, $\Delta K$.

$$\frac{da}{dN} = C (\Delta K)^m$$

(2.18)

where $da/dN = \text{fatigue crack growth rate (crack}$
growth per cycle),

\[ C_m = \text{material constants that depend on the frequency,} \]
\[ \text{temperature, environment, and stress ratio}, \]
\[ \Delta K = \text{range in stress-intensity}. \]

Unlike the variables of the other crack growth rate equations, the
stress-intensity factor reflects the effects of the external load, crack
size, and geometrical configuration on the intensity of the stress field
around the crack tip. Since the crack tip stress field distribution is
always the same, Paris et al. (44) felt that the stress field as
represented by \( \Delta K \) should control the rate of stable crack extension.

Three distinct crack propagation regions, as shown in Fig. 2.7, are
evident when the logarithm of the crack growth rate is plotted against
the logarithm of the stress-intensity range: slow crack growth in Region
I, linear stable crack growth in Region II, and unstable rapid crack
growth in Region III.

In the first region fatigue crack growth rate is very low. Below a
limiting threshold stress-intensity range, \( \Delta K_{th} \), cracks will not
propagate. Frost et al. (45,46) have shown that fatigue cracks, which
initiate at sharp notches, can remain stationary (or nonpropagating)
when the product of the edge crack length and the stress to the third
power are less than an experimentally determined material constant.
However, based on a comparison of the stress-intensity range and
experimental data, Barsom (47) concluded that \( \Delta K_{th} \) is independent of
chemistry, and depends primarily on the stress ratio, R, as described by

\[
\Delta K_{th} = 6.4 (1 - 0.85R) \text{ in ksi } \sqrt{\text{in}}
\]

\[
= 7.0 (1 - 0.85R) \text{ in MNm}^{-1.5}
\]  

(2.19)

when R is greater than 0.1. For value of R less than or equal to 0.1 the value of \(\Delta K_{th}\) is a constant equal to 5.5 ksi/\(\sqrt{\text{in}}\) (6.0 MNm\(^{-1.5}\)).

The transition from Region I to Region II is not distinct. Gurney (48) examined the propagation behavior of a range of steels and concluded that the change from the nonlinear curve at low crack growth rates to the linear branch of the curve given by Eq. 2.18 depends on the material and the magnitude of the applied stress. However, the growth of fatigue cracks at rates less than that of the linear, Region II behavior is not well understood, and additional research is necessary to define the lower transition zone.

Region II represents the zone of stable crack growth described by Eq. 2.18. Before the crack growth rate can be computed, however, the stress intensity range, \(\Delta K\), and the material constants, C and m, must be known.

Several methods can be used to determine the stress-intensity factor for a given configuration. A discussion of stress-intensity factors and a summary of various methods used to compute K-factors are given in Appendix B. Also included in Appendix B is an approximate method for computing \(\Delta K\), which depends primarily on the remotely applied stress
range, crack size, and corrections for free surface, crack shape, finite plate width, and nonuniform opening stresses.

A number of studies have been conducted to determine the material constants, \( C \) and \( m \), for a wide range of steels \((48,49,50,51,52)\). Both the slope, \( m \), and the scaling factor, \( C \), can vary significantly for a range of steel strengths. Differences in the size and distribution of microscopic grains have been cited as reasons for differences in the material constants \((53)\). However, Barsom \((54)\), in considering the differences in material constants, has suggested that the crack growth rates in martensitic steels are higher than those for ferrite-pearlite steels because crack branching (secondary cracking) in the ferrite-pearlite steels causes a reduction in the stress-intensity factor.

For a wide variety of metals the transition at high \( \Delta K \) values from stable crack growth in Region II to rapid crack growth in Region III has been found to be related to the crack opening displacement range, \( \Delta \delta \).

\[
\Delta \delta = \frac{\Delta K_I}{E S_{YS}} \quad (2.20)
\]

where \( \Delta \delta \) = crack opening displacement range,

\( S_{YS} \) = monotonic yield strength,

\( E \) = modulus of elasticity,

\( \Delta K_I \) = stress-intensity factor range.

The value of \( \Delta \delta \) at which transition occurs, \( \Delta \delta_T \), is essentially a
constant value equal to 0.0016-in. (0.041 mm) (54). For a range in stress-intensity factor greater than that corresponding to $\Delta K$, the crack growth rate increase rapidly. Fracture eventually occurs when $\Delta K$ equals the critical stress intensity factor $K_c$ (or $K_{IC}$).

Several additional factors can also affect the fatigue crack growth rate: mean stress, temperature, corrosive environments, random loads, load frequency, overloads, and welding residual stresses. The influence of these factors, however, depends on the magnitude or degree of their presence.

Mean stress is known to affect the rate of crack growth, and various methods of revising Eq. 2.18 to account for mean stress have been proposed (55,56). The rate of stable and rapid crack growth increases as the stress ratio, $R$, correspondingly increases. The threshold stress intensity factor is affected also by mean stress - Eq. 2.19. According to Elber (56) crack propagation can occur only when the crack tip is fully open. Hence, the crack growth rate depends on an effective stress-intensity range, $\Delta K_{eff}$, which is equal to the difference between the opening level, $K_{OP}$, and $K_{max}$. Along these lines, Hudson (57) found that crack growth rates are insensitive to compressive loading excursions when $R$ is less than zero, and that the fatigue response could be approximated by ignoring the negative portion of the loading cycle.

Corrosive environments can have significant detrimental effects on crack growth rates. For example, it has been shown that crack growth
rates in corrosive environments are markedly affected by the loading frequency (58). This makes correlations between the test results in an environmentally controlled laboratory and behavior in the field quite difficult to formulate, since the environment and loading rate in the field usually varies considerably. However, conservative crack growth rates can be selected to estimate the crack propagation lives of structural members in corrosive environments.

The fatigue lives of members subjected to random loadings have been successfully predicted using a damage criterion and a probability-density function to model the random loading histogram of a particular structure (59). Loading sequence effects are complex and not fully understood; Dowling (60) has reviewed a number of available techniques which consider the effects of loading sequence on the fatigue behavior.

Overloads can retard fatigue crack growth and prolong fatigue life by decreasing the crack growth rate in the overloaded plastic zone (61). For an overload ratio \( \frac{K_{OL}}{K_{max}} \) between 2.5 and 3.0 complete crack arrest can occur (62).

Crack propagation rates in both weld metal and the heat affected zone have been shown to be slowed by compressive residual stresses (50,51). The effect of welding residual stresses are discussed in greater detail in Chapter 4.
2.3.2 Methods for Applying the Propagation Model

The fatigue crack propagation life can be computed by transposing the variables of Eq. 2.18, and integrating over the entire crack width.

\[
N_p = \int_{N_I}^{N_T} dN = \int_{a_i}^{a_f} \frac{da}{C(\Delta K)^m} \quad (2.21)
\]

where 
\[
a_i = \text{initial half crack width,}
\]
\[
a_f = \text{final or critical half crack width.}
\]

In order to solve Eq. 2.21, the expression for the stress-intensity factor, which is a function of the crack size, must be known. Unfortunately, the stress-intensity factor is seldom a simple expression and a closed form solution of Eq. 2.21 is often difficult. Numerical integration is used, therefore, to estimate the propagation life. The crack width is divided into increments \(\Delta a\), small enough to minimize errors in the iteration procedure. Increments of the propagation life are then summed over the entire crack width.

\[
N_p = \sum_{a_i}^{a_f} \frac{\Delta a}{C(\Delta K_{\text{avg}})^m} \quad (2.22)
\]

where 
\[
\Delta a = \text{crack increment,}
\]
\[
\Delta K_{\text{avg}} = \text{stress-intensity range evaluated at } a_{\text{avg}},
\]
\[
a_{\text{avg}} = \text{average half crack width for the interval.}
A computer program has been prepared to evaluate the crack propagation life according to Eq. 2.22. Evaluation of the crack propagation lives for cracks initiated at weld discontinuities is discussed in Chapters 4 and 5.

It should be noted that Eqs. 2.21 and 2.22 are much more sensitive to the initial crack size than to the final crack size. An accurate estimation of the crack propagation life, therefore, depends primarily on an appropriate selection of the initial crack size.

2.4 Transition Crack Size

An important question regarding the engineering analysis of fatigue crack propagation behavior can be simply stated: "When does a crack become a crack?" The initial size of fatigue cracks which initiate at external surfaces or internal discontinuities is not clearly defined. However, two approaches for estimating the initial crack size have been used. The first approach involves the arbitrary selection of a small but reasonable crack size that would exist at an early stage in the fatigue life and would provide an accurate propagation estimate. A more recent approach involves the nonarbitrary selection of the initial crack size based on minimizing the total fatigue life at several elements in front of the notch (63). Using this method the initial crack size has been shown to depend on the stress level, notch size, notch geometry, and material.
2.5 Fatigue Behavior of Weldments with Discontinuities

The mathematical models outlined in this chapter will be used to examine the effect of weld discontinuities on the fatigue behavior of weldments. By treating discontinuities as notches, with a sharpness and size dependent on the discontinuity type, the fatigue crack initiation and propagation portions can be calculated using the procedures described above. The implementation of the mathematical fatigue models for weld discontinuities will be discussed in greater detail in Chapters 4 and 5.
3.1 Introduction

Experimental studies have been conducted in an effort to obtain a better understanding of the behavior of weldments which contain discontinuities. The experimental program was divided into three major areas: narrow width fatigue tests, full width fatigue tests, and residual stress examinations.

Zero to tension fatigue tests were conducted for plain plate and welded thin strip specimens. The specimens were termed "narrow width" since they were thin relative to the width of the full width fatigue test specimens. The purpose of the plain plate tests was to evaluate the mathematical models, to determine the most efficient method of notching the plates, and to provide for comparisons with similarly notched welded plates. The purpose of the welded narrow width tests was to examine the fatigue behavior of weldments containing a range of discontinuities. The discontinuities were modeled artificially using notches with various sizes, positions, and geometries.

Zero-to-tension fatigue tests were conducted also using large welded
specimens. The purpose of these tests was to examine the fatigue behavior of weldments which contained slag inclusion discontinuities. The tests were termed "full width" because the transverse butt-weld length was considerably longer than that of the narrow width specimens.

Residual stresses in the weldments were determined using a destructive sectioning technique. The residual stress distribution was determined for use in evaluating some of the variables which influence the weldment fatigue behavior.

3.2 Narrow Width Fatigue Tests

3.2.1 Plain Plate Specimens

The plain plate test specimens were fabricated from a 1-in. (25.4 mm) thick mild steel plate. Tensile test coupons were prepared and tested in accordance with ASTM Standard E8 (64) to define the mechanical properties of the mild steel.

The fatigue specimens were notched through the thickness with either a circular hole or a narrow slot. The notch position with respect to the width was varied using three positions for each notch type: midwidth, or offset from midwidth by 0.25-in. (6.35mm) or 0.50-in. (12.7mm). A sketch of the specimen dimensions and the notch sizes,
types, and positions is shown in Fig. 3.1.

Two different methods were used to form the circular and slotted notches to determine the effects of the notching method on the fatigue behavior. For one method the holes were drilled slightly undersized and reamed to the desired diameter. The slots were formed by first drilling a No. 65 hole - 0.036-in. (0.89 mm) diameter - in the plate so that a jeweler's saw blade could be used for cutting the desired slot width. For the second method, electrical discharge machining (EDM) was used to form both notch types. (The EDM process removes metal by utilizing the damaging effect of electric sparks, in a closely controlled manner, between two conducting surfaces immersed in a dielectric medium (65).) The EDM method was not selected because drilling, reaming, and sawing was quicker, more economical, and did not drastically alter the material properties near the notch.

3.2.2 Welded Specimens

The test specimens were fabricated from a 1-in. mild steel plate and a 1-in. HY-80 steel plate. A wide range in steel strengths was selected to examine the variability in the test results attributed to material strength. Mechanical and chemical properties of the mild steel, HY-80 steel, and the electrodes are listed in Table 3.1 and 3.2, respectively. The mild steel and HY-80 steel specimens were both manually welded using the shielded metal arc process. E7018 covered
electrodes were used to weld the mild steel, and E11018 electrodes were used to weld the high strength steel. Electrode diameters of 5/32-in. (4.0 mm) and 3/16-in (4.8 mm) were used for welding both steels. The welding procedures used to join the 1-in. plates are shown in Fig. 3.2. These procedures, designated P38-7018-A and P80-11018-A (Modified), were developed and extensively used at the University of Illinois for previous weld studies. To avoid unwanted porosity the electrodes were baked for one hour at 800°F (427°C) and stored in a holding oven at 300°F (149°C) until needed.

The notch geometries of the welded specimens were similar to those used for the plain plate specimens. The intent of the notches was to model a range in discontinuity sharpness and geometry. A smooth round hole represents the least severe stress raiser, while a sharp, flat slot represents a severe stress raiser; a crack, of course, is the most severe stress raiser. Two notch sizes and four positions through the specimen width (plate thickness) were used to model a range of discontinuity configurations. A sketch of the specimen dimensions, and notch geometries and positions is shown in Fig. 3.3.

Two 6-in. square plates were joined according to the welding procedures shown in Fig. 3.2. End blocks, attached to the groove ends, were used for weld bead starts and stops. After welding, the reinforcement was removed and the welds radiographed. In the weld regions that appeared to be free of discontinuities, the welded plate was sectioned longitudinally into 1/4-in. (6.3 mm) strips, Fig. 3.3d.
The strips were then surface ground to the final 3/16-in. (4.76 mm) specimen thickness. After notching the specimens, the surface near the notch was hand polished using several grades of emery paper. A polished surface was necessary to assist in the detection of fatigue cracks on the surface.

3.2.3 Testing Procedures

The fatigue machine used to test the narrow width specimens was the ± 50,000 lb. capacity servo-controlled hydraulic fatigue testing machine shown in Fig. 3.4a. The maximum load used in the tests was 5500 lbs. for the mild steel specimens and 8600 lbs. for the HY-80 specimens. The loading frequency was 200 cycles per minute (3.33 Hz).

Strain gages were attached to many of the specimens on both sides of the notch for assistance in detecting fatigue crack initiation. A gage was located also on the edge of the specimen, on the side closest to the notch, to examine the sensitivity of a surface gage to an internal crack. A 20X magnifying glass was used to examine the notch edges and the material at mid-thickness of the notches for evidence of crack initiation. For specimens with circular notches the number of applied stress cycles at which cracking was first detected was most reliably determined using the 20X magnifying glass; strain gages mounted on the specimen surfaces did not detect cracking before visual examination. For specimens with slotted notches both methods were equally effective.
The propagation of surface cracks was monitored with the traveling 40X microscope shown in Fig. 3.4b. Growth of a crack on the specimen surface was determined by measuring the movement of the microscope with two 0.001-in. (0.025 mm) scale dial gages. Between load applications a vertical crosshair in the microscope eyepiece was aligned with the crack tip, and the dial gage readings recorded.

The fracture surface of one plain plate specimen was examined using a scanning electron microscope. The spacing of striations on the fracture surface was used to estimate the applied range in stress-intensity factor.

3.3 Full Width Fatigue Tests

3.3.1 Specimen Description and Fabrication

Transverse butt-welded test specimens were fabricated from a 1-in. (25.4 mm) thick welded HY-80 steel plate. The plates were welded using the shielded metal arc process. Combinations of 1/8-in. (3.2 mm), 5/32-in. (4.0 mm), and 3/16-in. (4.8 mm) diameter E11018 covered electrodes were used for the welding. The electrodes were baked and stored as previously described.

A preliminary study was conducted to determine the best method of
altering the welding procedures to consistently produce the slag inclusion sizes desired. Pairs of 5-in. x 5-in. x 3/4-in. HY-80 steel plates with 60 degree double-V grooves were welded in the preliminary study. Radiographs were taken to evaluate the consistency of the slag length and width; longitudinal sections cut through the inclusions were used to evaluate the consistency of the slag shape. The method that most reliably produced slag inclusions with the desired length and size consisted of the following:

1. Deposit the root pass. Backgrind the first pass to sound weld metal to ensure adequate penetration of the second pass throughout the weld length.

2. Build up weld passes to the position of the desired discontinuity. (Passes 1 and 2 of Series VI in Fig. 3.5b.)

3. Reduce the welding current, causing the weld puddle to "roll over", and deposit one or more passes to one side of the weld groove. This will create a slag pocket between the weld bead and the opposite side wall of the groove. (Passes 4 and 5 of Series VI in Fig. 3.5b.)

4. Use an air-carbon arc to back-gouge all but the desired length of slag inclusion. Clean the gouged groove thoroughly.

5. Use small diameter electrodes to deposit sound weld passes in the notches formed by the gouging operation.

6. Deposit continuous weld passes over the remaining slag pocket, entrapping it.

A variety of slag inclusion configurations and geometries were examined. The full width fatigue testing program is summarized in Table 3.3. The welding procedure is summarized in Fig. 3.5a for Series Nos. I-V, and in Fig. 3.5b for Series Nos. VI and VII.
A number of 33-in. long x 1-in. thick HY-80 steel plates were cut in the middle, beveled, and then welded as shown in Fig. 3.6. The two halves of the plate were butted together, end blocks tacked onto each end of the groove, and welding performed in continuous passes along the groove width. After welding, the reinforcement was ground off and polished flush with the base metal. After radiographing the plates to locate the slag inclusions, the specimen blanks were situated such that the discontinuities were positioned at midwidth. The specimen blanks were cut from the plate and machined to a 5-in. (127 mm) width.

3.3.2 Description of Testing Equipment

The fatigue machine used in the investigation was the + 400,000 lb. capacity servo-controlled hydraulic fatigue testing machine shown in Fig. 3.7. The maximum load used in the tests was approximately 300,000 lb., applied at a loading frequency of 200 cycles per minute (3.33 Hz.).

Ultrasound examination of the test specimens with a 60 degree angle probe was used during various stages of the fatigue tests to detect crack initiation. Photographs of ultrasonic reflections from the slag inclusions were taken periodically during the fatigue tests. A definite change in shape and/or amplitude of the ultrasonic reflections from the initial recorded values was used to estimate the number of cycles necessary to initiate a fatigue crack.
Also, in Series I, radiographs were taken at various periods during the fatigue tests to detect the onset of fatigue crack initiation. However, the radiographs, which were taken under the maximum tensile load, did not successfully detect the onset of cracking in the tests (66); thereafter, no radiographs were taken during the fatigue tests.

For each specimen, 120 ohm electrical resistance strain gages, 1/8-in. (3.2 mm) gage length, were mounted on the weld centerline directly over the slag inclusions. A distinct change in the strain range was used to indicate crack initiation. For one specimen additional pairs of strain gages were attached to monitor bending stresses; the stress distribution was found to be relatively uniform with little bending.

3.4 Determination of Residual Stresses

An effort was made to determine the magnitude and distribution of the residual stresses in welded HY-80 steel plates. Residual stresses were determined from values of the strain relaxed when either small strips or blocks of material with strain gages bonded to the surface(s) were removed from the plates. A description of the methods used to determine residual stresses and a discussion of the results is given in Appendix A.
CHAPTER 4

DISCUSSION AND ANALYSIS OF TEST RESULTS

4.1 Introduction

In this chapter the results of the experimental program are presented and discussed. The experimental program collectively includes 61 fatigue tests and 4 residual stress tests. The fatigue tests were conducted using narrow and full width specimens containing either artificial or actual weld discontinuities. Strain relaxation measurements were used to determine residual stress distributions.

In Section 4.2 the results of strain relaxation measurements are presented, and the effect of residual stresses on weldment fatigue behavior are discussed briefly.

In the Section 4.3 and 4.4 the fatigue test results are presented, and then compared with the fatigue lives computed using the mathematical models outlined in Chapter 2.

In the final section, Section 4.5, the results and conclusions of the experimental study are summarized.
4.2 Residual Stress - Results and Effects on Fatigue

The magnitude of the residual stresses in a welded plate, typical of the welded HY-80 steel specimens, was determined from strain relaxation measurements. This information was used to assess the fatigue test results. (Residual stress distributions for mild steel weldments have been determined in other investigations and are readily available.) The methods used to determine the residual stresses are based on the strains relaxed on the surfaces of blocks removed from the welded plates. For the full width specimens the blocks were divided, and then sectioned in uniform increments towards the surface to determine the residual stress distribution through the thickness. A complete description of the methods used to determine the residual stresses is given in Appendix A.

The residual stress distributions in two narrow width strip specimens are summarized in Fig. A.3. One specimen was unnotched and had no prior loading history, while the other specimen contained a circular notch at midwidth and had been subjected to 3,416,750 cycles of loading; the first 2,110,000 cycles were applied under a stress cycle of 0.5 to 40.5 ksi (3.5 to 279.3 MPa), followed by 1,306,750 cycles at 0.5 to 45.5 ksi (3.5 to 313.7 MPa). The measurements indicate that a zone of compressive residual stresses existed in the middle portion of both specimens.

The residual stress distributions through the thickness of HY-80
plates with 5-in. (127 mm) or 25-in. (635 mm) long welds are shown in Figs. A.8 to A.11. The longitudinal and transverse residual stresses were determined at mid-length and at the ends of the welds. Results summarized in these figures indicate that: (a) residual stresses are higher in the plate with the longer weld, (b) the distribution of the residual stresses is unsymmetrical, (c) longitudinal and transverse residual stresses can be locally as high as the yield strength, and (d) the transverse residual stresses (transverse to the axis of the weld) are compressive near mid-thickness of the welds.

Several studies have been conducted to examine the effect of compressive and tensile residual stresses on the fatigue behavior of notched (67,68,69) and welded (70,71,72) specimens. The studies on notched specimens, with residual stresses induced by preloading, have shown that, compared to specimens without residual stresses, at long fatigue lives tensile residual stresses tend to reduce the fatigue life while compressive residual stresses tend to extend the fatigue life. Similar results have been found for welded plates. Gurney (70), for example, observed the beneficial effect of compressive residual stresses, induced by spot heating and local compression, in plates with fillet welded attachments. Trufyakov (71), using transverse, longitudinal, and transverse-longitudinal welded specimens, demonstrated that tensile residual stresses significantly reduced the fatigue life. Also, Serensen et al. (72) observed a fifty percent reduction in the fatigue strength as a result of tensile residual stresses for specimens with 9/64-in. (3.5 mm) weld undercuts; a thirty percent reduction in
the fatigue strength was noted for specimens containing 9/32-in. (7 mm) lack of penetration discontinuities located in zones of compressive residual stresses.

Another variable that will influence the extent to which residual stresses affect fatigue behavior is the cyclic relaxation of residual stresses, or mean stress relaxation. Mattos (40) proposed a relationship for which the cyclic decay of initial mean stress for A-36 steel is primarily a function of the applied strain amplitude. Using this relationship it can be shown that mean stresses relax more quickly for large strain amplitudes and high initial mean stresses.

It is clear that residual stresses can affect the fatigue life of a notched member in either a detrimental or beneficial fashion. For this reason it is important to consider the effect of residual stresses on the fatigue behavior of welded members containing discontinuities.

4.3 Narrow Width Fatigue Tests

4.3.1 Plain Plate Specimens with Notches

Zero to tension fatigue tests were carried out on 10 plain plate specimens with notches. The tests were conducted to evaluate the available methods for notching the plates and to provide a basis of
comparison for test results of similarly notched welded plates. The test results are tabulated in Table 4.1 and plotted in Fig. 4.1.

From the results shown in Fig. 4.1 it can be seen that notch sharpness and offset both affect fatigue life. Specimens notched with narrow slots (small notch root radii) have shorter fatigue lives than specimens notched with circular holes of the same diameter as the slot length; the lives differed by a factor of roughly 3 to 4. The fatigue life is affected also by a notch offset from midwidth. As indicated in Fig. 4.1 for both notch types, a greater reduction in fatigue life is obtained for large notch offsets from midwidth. Also shown in Fig. 4.1 is a mean fatigue curve based on a number of fatigue tests for mild steel specimens with a central circular hole (73). The fatigue lives for two of the three specimens with central circular notches are slightly less than one standard deviation below the mean curve, possibly indicating a somewhat lower fatigue resistance. The reduced fatigue resistance may be a result of differences in the steel material properties, since the mild steel used for the specimens had a lower yield and ultimate strength than either A7 or A36 steels (see Table 3.1).

The computed fatigue crack initiation lives of the plain plate specimens were based on the initiation model described in Chapter 2. The notch size and position in all of the fatigue tests were carefully measured to the nearest 0.001-in. (0.025 mm) to minimize errors in the analysis. The cyclic properties of A36 steel as obtained by Mattos (40)
were assumed to be valid, even though the steel used for the specimens did not completely satisfy ASTM-A36 (74) requirements for chemical and mechanical properties.

The appropriate (maximum) value of \(K_f\) must be used in the initiation analysis. For specimens with a circular notch the value of \(K_{f_{\text{max}}}\) was assumed equal to \(K_t\). This is a conservative but justifiable assumption: the notch root radius is large in comparison to the material constant, \(a^*\), in Peterson's equation, Eq. 2.4, so that the expression for \(K_t\) reduces approximately to \(K_t\). For specimens with an internal slot the value of \(K_{f_{\text{max}}}\) corresponds to the critical notch root radius, which is determined by equating the first derivative of \(K_t\) with respect to the notch root radius to zero, and solving for the corresponding critical value. The values of \(K_t\), which were used to compute \(K_f\), were based on known solutions (75) for the measured notch sizes and positions.

Comparisons between the computed fatigue crack initiation lives and the number of stress cycles applied before cracking was first detected are shown in Fig. 4.2 The computed and observed initiation lives compare reasonably well. However, for the three specimens which have the least severe stress raisers, the calculated initiation lives are greater than the observed lives. Possibly, one reason for inaccuracy of the fatigue initiation model at longer lives is related to the strain amplitude predicted by the Neuber analysis; Neuber control for A-36 steel properties is satisfied at a higher stress and lower strain than for the actual mild steel.
The fatigue crack propagation lives were computed in two stages: the life to propagate a small (initiated) elliptical crack through the thickness of the specimen, and the life required to propagate a through-thickness crack to failure. The crack propagation lives of the specimens for both stages of the crack growth were calculated using Eq. 2.22. The variables which primarily influence the computed results include the type and size of initiated fatigue crack, its corresponding range in stress-intensity factor, and the material crack growth rate.

Two types of fatigue cracks for the initiation analysis were considered: an elliptical crack at mid-thickness of the notch and a circular corner crack at the notch edge. Based on a stress analysis by Sternberg et al. (76), who demonstrated that $K_t$ for a circular hole in a plate is largest at mid-thickness of the notch and smallest near the surface, fatigue crack initiation is most likely to occur at mid-thickness of the notch. For each crack type the stress-intensity factors were computed using the approximate method described in Appendix B.

The initial crack size was arbitrarily assumed equal to 0.01-in (0.25 mm) for the mild steel specimens. This value has been used previously by Mattos (40) and Lawrence (77) in modelling the propagation fatigue lives of mild steel specimens.

The fatigue crack growth rate in the linear region, given by Eq. 2.18, is governed by the crack propagation material constants $C$ and $m$. 
To determine these constants, the crack growth for each specimen was monitored periodically during the test and plotted against the number of applied stress cycles. One such plot for Specimen PP-2 is shown in Fig. 4.3. The crack growth rate at a particular crack depth is equal to the slope of the crack growth curve; finite difference techniques were used to compute the crack growth rate at each measured crack depth. Since the crack growth rate is known for each measured crack depth, a relationship between the range in stress-intensity factor and the crack growth rate can be established by computing the stress-intensity factor corresponding to each measured crack depth. Comparisons of the computed range in stress-intensity factors and the crack growth rates for Specimens PP-1, PP-2, PP-3, PP-4, and PP-9 are shown in Fig. 4.4. Data from the other specimens were not included since the range in stress-intensity factors were altered by an offset from midwidth. Only data for crack growth rates greater than $5(10)^{-6}$ in./cycle ($12.7(10)^{-5}$ mm/cycle) are shown, since this crack growth rate corresponds approximately to a stress-intensity factor for a full through-thickness crack. Also shown in Fig. 4.4 is the linear regression of log da/dN on log $\Delta K$ for the fatigue data, and the best fit line suggested by Barsom (49) for ferrite-pearlite steels. The mild steel specimens exhibited a higher crack growth rate than the value suggested by Barsom.

Comparisons between the computed and the observed fatigue crack propagation lives are shown in Fig. 4.2. The triangular symbols in this figure, which represent the computed crack propagation lives, compare very favorably with the observed crack propagation lives with one
exception. The crack propagation life of Specimen PP-9 was nearly six times greater than the computed crack propagation life, and can most likely be attributed to a delay in the fatigue crack growth as a result of two tensile overloads accidently applied to the specimen. This observation is consistent with studies by von Euw et al. (62) and Abtahi et al. (61); both have reported extended fatigue crack propagation lives as a result of delayed crack growth rates resulting from tensile overloads.

Comparisons between the observed and computed total fatigue lives are shown in Fig. 4.5. As noted above, the fatigue life of Specimen PP-9 does not agree favorably with the computed value as a result of tensile overloads. However, the remaining tests agree reasonably well; the computed fatigue lives are slightly less than the observed lives for all but three of the specimens.

The fatigue mathematical models are in reasonable agreement with the experimental results, indicating that the mathematical models are valid for use in examining fatigue data. However, before examining the fatigue behavior of welded members, the mathematical models must be modified to account for additional complicating factors such as welding residual stresses, weld discontinuities, and variations in the mechanical and cyclic properties. These modifications are considered in the following sections.
4.3.2 Welded Specimens

Zero to tension fatigue tests were conducted for 27 transverse butt-welded specimens; thirteen specimens were fabricated from mild steel plate, and fourteen specimens were fabricated from HY-80 steel plate. The tests were conducted to examine the accuracy of the mathematical models in describing the fatigue behavior of notched and welded plates subjected to a constant fluctuating load. The test results are tabulated in Tables 4.2 and 4.3 and plotted in Figs. 4.6 and 4.7.

The diversity of the fatigue test results shown in Figs. 4.6 and 4.7 is immediately evident. Factors responsible for the scatter include residual stress (magnitude and direction) and notch type, position, and size. Effects on the fatigue behavior resulting from notch position and residual stress magnitude are interrelated since both depend upon location through the thickness.

The fatigue test results of the welded mild steel and HY-80 steel specimens are very similar in general behavior. From the results plotted in Figs. 4.6 and 4.7 the following observations can be made:

1. All but two of the eight specimens with notches at location A (midwidth) did not fail after 2,000,000 or more cycles. The two specimens which did fail, however, did so as a result of small unintentional discontinuities in the welds. (See Fig. 3.3 for notch types and locations.)

2. Of the four specimens with notches at location B, only the HY-80 welded specimen with a slotted notch failed.
3. All but one of the fifteen specimens with notches at locations C or D failed.

4. Failure, when it occurred, proceeded in an orderly fashion. The specimens containing the greatest notch size and notch offset from midwidth had the shortest fatigue lives for each notch type.

5. The fatigue lives of the specimens containing slotted notches were less than the lives of the specimens containing circular notches for similar notch locations.

6. The fatigue lives of several specimens were greater than the mean fatigue lives of corresponding as-rolled plain plate material.

Based on the first four observations it is clear that notch position and residual stress distribution significantly affected the fatigue behavior. Specimens containing notches near midwidth either did not crack or did not permit the advance of an initiated fatigue crack to failure. By offsetting the notch to at least quarter-thickness nearly all the specimens failed. This behavior is consistent with the residual stress distribution effect outlined in the previous section: compressive residual stresses near mid-thickness act to prolong the fatigue life, while tensile residual stresses near the surface act to reduce the fatigue life.

The last two observations were also significant. An increased notch sharpness will result in a shorter fatigue life. Also, welded specimens with fatigue lives greater than the lives indicated by the mean fatigue curve for as-rolled plain plate is not unreasonable since the weld reinforcement was ground off, the mild scale removed, and the specimen
surfaces polished.

For Specimens M-4, M-5, T-1, and T-14 small unintentional discontinuities were severe enough stress raisers to act as preferred fatigue crack initiation sites in lieu of the circular through thickness notches. For these four specimens fatigue cracks initiated at small slag inclusions located close to the surface and slightly offset from midwidth for both the mild steel specimens, Fig. 4.8, and the HY-80 steel specimens, Fig. 4.9. Undoubtedly, the initiation zones were influenced by the increased stresses near the notch, a sharper notch root radius, and an unfavorable residual stress distribution. The unintentional discontinuities were not visible on the radiographs of the welds prepared for specimen fabrication. It should be noted also that detrimental effects of small unintentional discontinuities, acting as additional or even preferred fatigue crack initiation sites, have been observed in several other investigations (78, 79, 80, 81, 82, 83).

Two different methods were used to examine parameters related to the fatigue behavior. A scanning electron microscope (SEM) was used to examine the fracture surface of one specimen, and a hardness survey of the weld zones was conducted for two of the specimens.

A JSM-25 Scanning Electron Microscope was used to examine Specimen M-6 for evidence of striations on the fracture surface. The presence of striations on a fracture surface is an indication of fatigue crack propagation. The region examined with the SEM, an interior point of the
notch near the edge, is shown in Fig. 4.10a. Photographs of the region at magnifications of 1000X and 7000X are shown in Figs. 4.10b and 4.10c. Striations evident in these photographs clearly indicate that fatigue crack growth had occurred in the examined region.

Bates and Clark (84) proposed a relationship between the spacing of fatigue striations and the range in stress-intensity factor.

\[
\text{Striation Spacing } \sim 6(\Delta K/E)^2 \tag{4.1}
\]

By using the \(\Delta K\) expression from Appendix B for a circular hole in a plate, a stress range of 22.7 ksi (156.8 MPa) corresponds to a \(\Delta K\)-value of 34.9 ksi in (38.4 MPa m). From the measured striation spacing the \(\Delta K\)-values computed using Eq. 4.1 were between 34.1 ksi \(\sqrt{\text{in}}\) (37.5 MPa m) and 41.8 ksi \(\sqrt{\text{in}}\) (45.9 MPa m). The agreement in \(\Delta K\)-values indicates that the approximate method for determining the stress-intensity factor provides a good estimate of the actual value.

A hardness survey of two welded specimens, one mild steel and one HY-80 steel, was conducted along lines perpendicular to the weld grooves and immediately adjacent to the circular notches. The results are summarized in Figs. 4.11 and 4.12. The hardness values are related approximately to the ultimate tensile strength (85); Tables 3A and 3B in A370 (86) provide approximate hardness conversion numbers for nonaustenitic steels. Since the ultimate tensile strength is related to fatigue strength (87), the hardness provides an indirect estimate of the fatigue strength of the base metal, heat affected zone (HAZ), and weld
metal. The hardness values of the mild steel specimens shown in Fig. 4.11 vary somewhat but the HAZ and weld metal appear to be slightly harder than the base metal. The hardness values of the HY-80 steel specimens shown in Fig. 4.12 change more rapidly, with the HAZ near the fusion line being harder than the adjacent weld metal, and the weld metal and HAZ both harder than the base metal.

Several of the specimens with notches near midwidth did not fail. Three of the welded specimens which contained central slotted notches had cracks at the notches that appeared not to propagate; seven specimens did not crack at all. Compressive residual stresses at the notch roots were believed to be the reason crack initiation and/or crack propagation had been delayed. To examine this suspicion, the values of $\Delta K$ for the specimens with cracks at the notches were computed on the basis of a total absence of residual stresses. In all cases the computed values of $\Delta K$ were much greater than $\Delta K_{\text{th}}$ given by Eq. 2.19, which implies that without residual stresses the cracks would have propagated. To determine whether the residual stress had arrested or only delayed the fatigue crack growth, the fatigue cycling of one specimen was resumed. The crack sizes for Specimen T-11, which had been previously subjected to 2,210,000 loading cycles, were measured from microphotographs (215X). The specimen was then placed back in the testing machine, and after 4,856,800 additional loading cycles the specimen was removed, rephotographed, and the cracks remeasured. The fatigue cracks had grown by an average of only 0.00023-in. (0.0059 mm). It is clear that compressive residual stresses severely inhibited
propagation of the fatigue cracks at notches located near mid-thickness.

The fatigue crack initiation lives for the welded specimens were computed using the fatigue and cyclic properties of weld metal. The cyclic and fatigue properties of E60 and E110 weld metal as determined by Higashida (88) were used to examine the fatigue behavior of the welded specimens containing internal discontinuities. The use of E60 weld metal properties may introduce slight inaccuracies in the crack initiation analysis of the welded mild steel specimens since E7018 electrodes were actually used for the welding.

The possible effects that residual stresses, $S_r$, at the notch root can have on the fatigue crack initiation life may be bounded by assuming: (a) a detrimental effect with $S_r = + S_{YBM}$, (b) a neutral effect with $S_r = 0.0$, or (c) a beneficial effect with $S_r = - S_{YBM}$. The applicability of each assumption depends on the discontinuity (notch) location relative to the plate thickness. By using a set-up cycle, which is actually no more than a mechanical prestressing, either tensile or compressive residual stresses can be introduced at the notch root (89).

The fatigue crack propagation lives for the welded specimens were computed on the assumption that cracks would propagate at a rate equivalent to the crack growth rate of the accompanying base metal, which is consistent with observations by both Maddox (90) and Dowse et al. (91). Much slower crack growth rates in welds, however, have
been observed as a result of the beneficial effect of compressive residual stresses (50,51). In the study by Parry et al. (50) two distinct crack growth rates were observed for virtually identical plates and welding procedures; one crack growth rate was similar to that of the base metal, the other was much less. Upon stress relief heat treatment, however, the slower crack growth rates increased to the crack growth rates of the unwelded steel. The assumption of crack growth rates equivalent to the base metal, although sometimes conservative, is realistic and justified in view of the behavior observed by Parry.

Comparisons between the observed and the calculated fatigue crack initiation, propagation, and total fatigue lives are shown in Figs. 4.13, 4.14, and 4.15, respectively.

In Fig. 4.13 the open symbols represent the values of $N_I$ computed assuming no residual stress exists at the notch root, while the darkened symbols represent the values of $N_I$ computed assuming compressive residual stress exist at the notch root of yield-point magnitude of the base metal. The effect of a tensile residual stress was not considered because the values of $N_I$ computed on the basis of no residual stress at the notch root were less than the observed crack initiation lives for all welded specimens.

In Fig. 4.14 the open symbols represent the values of $N_p$ computed for crack propagation rates equal to the unwelded steel, while the darkened symbols represent the values of $N_p$ computed for crack
propagation rates slowed by compressive residual stresses. The crack
growth rate constants which reflect the effect of compressive residual
stresses were obtained from Ref. (51) for E70 weld metal and from Ref.
(50) for E110 weld metal. Tensile residual stresses were not
considered.

The computed total fatigue lives are compared to the observed
fatigue lives in Fig. 4.15. As before, the open symbols in this figure
refer to a zero residual stress effect, and the darkened symbols refer
to a full compressive residual stress effect. The total fatigue lives
computed on the basis of negligible residual stresses underestimate the
observed fatigue lives for all welded specimens. Agreement between the
observed and computed fatigue lives for specimens containing a notch
near midwidth was improved significantly by assuming that a compressive
residual stress of yield point magnitude existed at the notch root for
the initiation analysis. However, for specimens with notches offset to
roughly quarter-width, the fatigue lives computed assuming full
compressive residual stress at the notch root are greater than the
observed fatigue lives. These results are consistent with previous
observations concerning the effect of residual stresses. The transition
from compressive to tensile residual stresses occurs at approximately
one-third to one-quarter of the thickness, and explains why the observed
fatigue lives fall between the fatigue lives computed on the basis of
either zero or fully compressive residual stresses.

In general, the fatigue models underestimated the actual fatigue
lives of the welded specimens. Discounting the uncracked fatigue specimens, most of the computed fatigue lives shown in Fig. 4.15 fall within a factor of five of the observed lives. However, the computed fatigue lives compare more favorably with the observed behavior when the initiation analysis includes a residual stress distribution consistent with the specimen notch position.

4.4 Full Width Fatigue Tests

4.4.1 Fatigue Test Results

Zero to tension fatigue tests were carried out on 24 welded HY-80 specimens containing slag inclusion discontinuities. The tests were conducted to examine the influence of various slag inclusion sizes, shapes, and geometric configurations on the fatigue behavior. The test results are tabulated in Table 4.4 and plotted in Fig. 4.16.

Comparisons of the data plotted in Fig. 4.16 indicate general trends of the individual test series. For example, the fatigue lives of specimens with only one discontinuity (Series I, III, and VII) are greater in all cases than the fatigue lives of specimens with two discontinuities (Series IV, V, and VI). This suggests that, in addition to interaction effects between multiple discontinuities, a reduction in area on planes perpendicular to the direction of loading also has an
effect on the fatigue life. Hartmann et al. (92) observed an effect of
the reduction in area (based on radiographic examination) on the fatigue
strength at 100,000 cycles. A plot similar to that given in Hartmann's
study is shown in Fig. 4.17 for the data gathered in this investigation;
the percent reduction in area, however, was measured on the fracture
surface, and the fatigue strengths at 100,000 cycles were computed
assuming an S-N slope of -0.23. The data clearly falls within the bands
suggested in Ref. (92).

Strain gage and ultrasonic techniques were used to estimate the
fatigue crack initiation lives. The fatigue crack initiation estimates
using these techniques, as well as the number of stress cycles applied
before visual detection of surface cracking, are summarized in Table
4.5. Change in the ultrasonic pulse was consistently more successful in
detecting fatigue crack initiation than change in the surface strain
range. Most likely this is related to the ability of an ultrasonic
probe, monitored at several grid locations, to cover a wider area of
material near the notch root than a strain gage bonded to the surface at
one particular location. Strain gages, however, can be effectively used
to detect crack initiation if the initiation zone is located close to
the strain gage.

To obtain an estimate of the surface strain gage sensitivity, the
crack position for a mild steel, narrow width notched specimen was
compared to the range in edge strains, Fig. 4.18. The data for the
change in strain range indicates that the surface gage first detected
the fatigue crack when it was 0.31-in. (7.9 mm) from the edge. However, the distance between a fatigue crack and the surface strain gage at first crack detection for the full width HY-10 steel specimens will be somewhat different as a result of plain strain constraint and different material stress-strain response.

Although fatigue cracks initiated sooner at discontinuities close to the surface, the fatigue lives of these specimens were similar to the fatigue lives of specimens with discontinuities at the one-third thickness position. One plausible explanation for this behavior is that cracks near the surface had to propagate through a wider zone of compressive residual stress, thereby slowing the crack propagation rates and extending the fatigue lives.

Specimens with pairs of discontinuities along the weld axis severely reduced the fatigue lives compared to specimens containing single inclusions located at an identical position through the thickness. Increasing the slag spacing from 1/2-in. (12.7 mm) to 1-in. (25.4 mm) between the discontinuities aligned along the axis of the weld resulted in reduced fatigue lives. However, this is believed to have been the result of a greater effective crack length composed of coalesced cracks which initiated at the main discontinuities and at unintentional discontinuities located between the two slag lines. The fatigue lives of specimens containing slag lines aligned through the thickness, Series VI, were comparable with the fatigue lives of specimens containing two slag lines aligned along the axis of the weld. Again, it is believed
that this behavior is related to the propagation of fatigue cracks through a compressive residual stress zone.

4.4.2 Analysis of the Data

The discontinuity sizes were measured from the fracture surfaces and from radiographs taken prior to testing to assist in the fatigue crack initiation and propagation analyses. This information was used to compute values of $K_{I_{\text{max}}}$ for the fatigue crack initiation analyses, and $F_G$, the nonuniform opening stress correction factor, for the fatigue crack propagation analyses.

The fatigue crack initiation analyses were conducted using the fatigue and cyclic properties of E110 weld metal, and the assumption that no residual stresses existed at the notch root. Comparisons between the computed and observed fatigue crack initiation lives are summarized in Fig. 4.19. The computed initiation lives are less than the observed crack initiation lives for all but five of the fatigue tests. Of these five specimens, however, three are within 1.25 of the computed initiation lives, and two within 1.6.

The fatigue crack initiation lives were not computed on the basis of a tensile or compressive residual stress at the notch root since reasonable results were obtained by ignoring the residual stress at the notch root. Favorable comparisons are believed to be the result of two
factors: (a) the discontinuities were positioned in a transition zone where residual stresses are quite low, and (b) the magnitude of the applied tensile stresses were high and tended to quickly wash out the residual stresses.

The fatigue crack propagation analyses were conducted using the crack propagation constants for unwelded martensitic steels. The life was computed in two stages: the life to propagate an initiated fatigue crack to the surface, and the life to propagate a surface crack through the thickness of the specimen. During the fatigue tests it was observed that once a fatigue crack had propagated through the thickness, very little life remained. Comparisons between the observed and computed fatigue crack propagation lives are shown in Fig. 4.19. Most of the computed crack propagation lives are within a factor of 1.5 of the observed crack propagation lives.

Comparisons between the computed and observed total fatigue lives for the full width specimens are shown in Fig. 4.20. The comparisons indicate that the fatigue behavior predicted by the mathematical models agree favorably with the fatigue behavior of weldments containing slag inclusion discontinuities.

4.5 General Remarks

Differences in the fatigue behavior of the narrow width and full
width specimens are the result of several factors: discontinuity (notch) types, net section stress range, constraint and through thickness stresses, residual stress distributions, and the type and shape of the fatigue cracks. Success in predicting the fatigue behavior of the specimens depends on realistically incorporating the influential factors into the mathematical models. This was accomplished in the following fashion: the effect of residual stresses were introduced by modifying the notch root stresses and the crack propagation rate constants; the influence of discontinuity size and position were reflected in the expressions for $K_{f_{\text{max}}}$ and $\Delta K$; the effect of constraint was introduced by modifying the $\Delta K$ expression; and assumptions of crack shapes were based on the crack initiation location relative to the thickness and/or width.

Based on the limited results of the experimental fatigue testing program three general conclusions can be stated. First, small unintentional discontinuities that can not be detected by radiography can influence the fatigue behavior, especially at high stress levels. Second, the discontinuity size and location acts in tandem with the corresponding notch root residual stress magnitude and direction in controlling weldment fatigue behavior. Third, the fatigue lives of welded members containing weld discontinuities of known sizes and positions can be realistically estimated using the mathematical fatigue models described hereinbefore.
5.1 Introduction

In this chapter methods for describing the fatigue strength of steel weldments which contain discontinuities are examined, and a fatigue based weld quality relationship is suggested. The relationships, which directly relate fatigue strengths to weld qualities, should not be thought of as a design standard per se. However, fitness-for-purpose standards should play a more influential role in assessing allowable discontinuity levels rather than the arbitrary weld quality requirements now found in many specifications.

Various types of discontinuities which are inadvertently encountered in normal welding operations are examined. Curves which relate discontinuity severity and reduction in the mean fatigue strength are formulated on the basis of mathematical fatigue models. From these curves, a simple-to-use method is suggested, relating the level of weld quality to a reduction in the mean fatigue strength of a sound weld. The suggestions allow a designer to stipulate desired levels of weld
quality which must be satisfied by the fabricator to achieve a given level of mean fatigue strength.

Section 5.2 describes the methods and limitations of the rating-criteria model. Sections 5.3 and 5.4 describe the fatigue strength reduction curves for specific nonplanar and planar weld discontinuities. Section 5.5 discusses the methods used to condense the fatigue models into a workable weld quality-fatigue strength relationship. A summary of the recommendations in tabular and graphic form is presented also.

5.2 Fatigue Rating Criteria Models

The fatigue strength of weldments which contain discontinuities can be rated on the basis of an appropriate discontinuity parameter. Usually, the predominant discontinuity parameter affects the overall fatigue strength, while other factors often have only secondary effects. Rating criteria are established by developing curves relating the reduction in mean fatigue strength to the discontinuity severity. Only common discontinuities such as lack of fusion, lack of penetration, undercut, slag inclusion, and porosity are considered.

It is well known that the fatigue strengths of welded members containing internal and surface discontinuities are affected by welding residual stresses (67,68,69,70,71,72). The role that residual stresses
play in influencing the fatigue strength of weldments which contain discontinuities depends on factors such as the material type, load history, welding procedure, and discontinuity shape and through thickness position. The inclusion of residual stresses, if known, would provide for the most accurate fatigue analysis. However, the magnitude and distribution of residual stress varies for different welding procedures, and may even vary with time. Therefore, the rating criteria described in this chapter are based on ignoring the effects of welding residual stresses.

The fatigue strength of a weldment that contains a discontinuity can be rated by direct comparison with the baseline fatigue strength of a (sound) weldment which contains no discontinuities. Such a comparison will indicate the percent reduction in mean fatigue strength attributed to a particular discontinuity severity. Several groups of data were examined to determine what constituted sound weldment fatigue behavior. Best-fit mean fatigue curves were determined by Munse et al. (73) based on more than 2100 fatigue tests of mild steel and quenched and tempered steel butt welded specimens with the reinforcement either intact or removed. Groups of data for welds with known weld discontinuity severities were then compared to the overall fatigue behavior of a large data bank of fatigue test results compiled by Munse. An example of a comparison between a group of fatigue test results with weld discontinuities and the mean (data bank) fatigue curve is shown in Fig. 5.1. The curve at one standard deviation above the mean data bank fatigue curve exceeds most of the fatigue data for weldments which
contained even small weld discontinuities. Two results from Fig. 5.1 are evident: (a) some data for specimens containing weld discontinuities fall above the mean data bank fatigue curve, implying that the data bank mean curve is based on a wide variety of tests including both sound welds and welds containing discontinuities; and (b) that a curve at one standard deviation above the mean data bank fatigue curve appears to fall above almost all fatigue data for specimens containing known weld discontinuities, implying that the one standard deviation curve may represent sound weldment behavior.

It should be noted that the log S-log N curve for plain plate was not used to represent the baseline mean fatigue curve. The long life fatigue strengths of plain plate are high compared to the data bank weld fatigue test results. Differences in the fatigue behavior of sound welds and plain plate material occur at long lives because the presence of the weld metal and the heat affected zone, with grain structures and material properties different than that of the base metal, tend to reduce the fatigue resistance when compared to plain plate (93).

Methods for determining fatigue strength reductions for various weldment discontinuity severities are examined in the following two sections. Generally, it has been observed that the fatigue strengths of weldments containing nonplanar discontinuities are best described by empirical relationships, while the fatigue strengths of weldments with planar discontinuities are best described by theoretical fatigue models. For each discontinuity type previous experimental data will be reviewed,
the proposed fatigue models will be discussed, and a fatigue strength rating curve will be presented.

5.3 Nonplanar Weld Discontinuities

Nonplanar weld discontinuities are, as the name implies, three-dimensional weld discontinuities which are not physically oriented on any one given plane. Gas porosity and slag inclusions are two common types of nonplanar weld discontinuities. The effects of porosity and slag on the fatigue behavior are examined in this section.

5.3.1 Porosity

The formation of porosity occurs when elements present in either the base metal, filler metal, or in the atmosphere react and release gases which do not escape from the molten weld metal before it solidifies. Several of the factors responsible for the formation of porosity include weld cooling rates, heat input, weld penetration, weld cleanliness, and welding current, speed, and arc length.

The effect of porosity on the static strength depends on the degree of porosity present in the weld. Green et al. (8) demonstrated that for mild steel welds porosity did not affect the mechanical properties until a porosity severity of 7 percent of the cross-sectional area was
present. For reductions in the cross sectional area greater than 7 percent the strength and ductility steadily decrease.

In an effort to classify the fatigue behavior of weldments containing porosity, Harrison (94) examined a large group of fatigue data divided according to the percent volume of porosity contained in the weldments. By using an arbitrary set of parallel log $S$-log $N$ curves at slopes of $-0.25$, Harrison noted that the position of the fatigue curves corresponded to particular levels of the percent volume of porosity. A family of four log $S$-log $N$ curves were used to separate five areas, labelled V through Z, called "quality bands"; by assigning values of the percent volume of porosity to each quality band the fatigue behavior is described. The V-band corresponds to the best weld quality and the Z-band refers to the poorest weld quality. This method allows no discontinuities to exist in Category V, with increasing discontinuity severities permitted in categories W through Z. The volumes of porosity corresponding to each quality band as recommended in Ref. (94) are summarized in Table 5.1.

To examine the accuracy of the quality band approach a number of fatigue studies with weldments containing known amounts of porosity were compared to the recommended quality band values (81,82,83,92,95,96,97); an example of a comparison between the quality band values recommended by Harrison and one group of fatigue test data is shown in Fig. 5.2. Although the data shown in Fig. 5.2 does not always closely fit the quality level, it does usually have a fatigue strength which is greater
than the quality band value. This result is expected, however, since
the quality band porosity levels were selected low enough to bound all
data above the recommended discontinuity severities.

The recommendations made by Harrison are based on the percent volume
of porosity. For uniform porosity, the percent reduction in volume is
equal to the mean percent reduction in cross sectional area (94). Another
method for evaluating the amount of uniform porosity present in
a weldment is the pore counting system proposed by Norrish and Moore
(98), which is used to relate the discontinuity size to a porosity
severity score.

Other recommendations suggested by Harrison for using quality bands
to rate porosity fatigue effects include: expressing porosity clusters
as a percentage of the volume of the weld metal containing the cluster;
treating linear porosity parallel to the weld axis on a percentage basis
only when it can be demonstrated that lack of root penetration does not
exist; and only using the quality band approach for porosity severities
less than 20 percent, since other more harmful discontinuities most
likely exist for greater porosity severities.

The most satisfactory nondestructive testing technique for
detecting porosity is radiography, which supplies a 2-dimensional
representation of a 3-dimensional situation. In order to accurately
determine the volume of the discontinuity, at least two radiographs must
be taken at different orientations for the length, width, and thickness
to be established. However, this is not always practically possible and, generally, only one radiograph is taken normal to the surface of the member. Although the thickness of the discontinuity is not known when only one radiograph is taken, a good estimate can be made with the following simplifying assumptions:

1. Porosity clusters and pores that appear circular on the radiograph can be assumed spherical of the same diameter (Fig. 5.3a).

2. Porosity clusters and pipes that appear rectangular on the radiograph can be assumed cylindrical with a circular diameter the same as the width (Fig. 5.3b). Further refinement is possible if the thickness of the porosity cluster is demonstrated to be limited to a certain weld bead thickness, so that the cylinder has an elliptical cross section (Fig. 5.3c).

A rating curve for porosity can be obtained by comparing the fatigue strength for a sound weldment to the fatigue strengths of the quality bands at the recommended discontinuity levels; the strengths should be compared at different fatigue lives since the slope of the quality bands and the slope of the mean fatigue curve are different. The two parameters necessary to describe the position of a linear log S-log N curve are the slope and the scaling factor. The slope of the quality bands as suggested by Harrison is -0.25. The scaling factor is equal to the logarithm of the fatigue life at a stress range of one unit. A polynomial curve was fitted through the recommended porosity values for qualities V through Y to obtain a relationship describing the scaling factor as a function of the volume of porosity. These curves are shown in Fig. 5.4 for both the reinforcement intact and removed. The scaling factor for V-quality was selected by passing a log S-log N curve,
parallel to the quality bands, through the mean fatigue curves for sound weldment behavior at two million cycles. This ensures that the curve for V-quality, as defined, will contain no discontinuities since it lies entirely above the fatigue curves for sound weldment behavior for lives less than 2 million cycles.

Comparisons between the quality band fatigue strength and the fatigue strength for sound weldment behavior at 100,000 and 2,000,000 cycles are shown in Figs. 5.5 and 5.6. These curves directly relate the percent volume of porosity entrapped in a weldment to the percent reduction in mean fatigue strength of a sound weldment with the reinforcement either ground off or left intact.

5.3.2 Slag Inclusions

Slag is a nonmetallic material which comes from the protective covering in the shielded metal arc process, or from the flux in the submerged arc or flux-core arc welding processes. A slag inclusion occurs when slag becomes entrapped within or beneath a weld bead as a result of rapid weld cooling rates, poor groove configurations, or inadequate cleaning of weld surfaces. Slag inclusions occur, or at least are detected, more frequently than most weld discontinuities.

The fatigue behavior of weldments which contain slag inclusions have been classified by Harrison (99) in a manner similar to that used to
classify porosity, except that the length of entrapped slag perpendicular to the applied loading is used in assessing the discontinuity severity. Table 5.1 summarizes the slag inclusion lengths recommended in Ref. (99) for each quality band value.

It should be recognized at the outset that slag inclusion length is not the only factor which influences the fatigue behavior of a weldment containing slag. Recent tests at the University of Illinois (100) indicate that the slag inclusion length, shape, diameter, position relative to the weld surface, and sharpness perpendicular to the applied loading all influence the fatigue behavior. It was found that, for slag inclusions at quarter-thickness with equal lengths, welds with small diameter (sharper) inclusions had lower fatigue lives than welds containing larger diameter slag inclusions. However, it was found also that multiple slag inclusions drastically reduced the fatigue strength. This indicates that the slag inclusion length, which is related to the percent reduction in cross-sectional area, plays an important role in the weldment fatigue strength also.

Fatigue tests at the Welding Institute in England also reflect the importance of parameters other than slag length. For weldments containing continuous slag lines, Harrison et al. (79,80) found (a) that very small inclusions, with weldment stress relief, often become initiation points, indicating the importance of slag diameter and sharpness, and (b) that the fatigue strength was higher for weldments containing slag inclusions near mid-thickness than for weldments with
slag near the surface, indicating the importance of the slag through thickness position.

In spite of the influence of the above factors on the fatigue behavior of weldments which contain slag inclusion discontinuities, a few reasons still exist which make use of slag inclusion length advantageous. These include:

1. The variability of slag shape and depth through the thickness make inclusion of all the factors into a general fatigue classification quite difficult.

2. Slag shape is not uniform along the length, and assumption of the worst configuration for the entire slag length may be too conservative for an initiation and propagation analysis.

3. Stereoradiography, which is costly, would be required to define the shape and depth of slag inclusions for evaluation by a more exact fatigue analysis.

4. The length and width of a slag inclusion is easy to determine from a single radiograph taken normal to the weld axis.

5. The slag inclusion length can provide an estimate of the percent reduction in area since the width is known from a normal radiograph and the depth is generally limited to one weld pass. See Fig. 4.17.

The difficulties in defining the parameters other than slag length and width add to the complexity of properly evaluating the fatigue life of a weldment containing slag inclusions. For this reason, the use of a fatigue strength relationship based strictly on slag length will be subject to error depending on the relative importance of the additional parameters which have been ignored.

Two or more slag inclusion discontinuities can conceivably, if they
are close enough together, act as a single large inclusion. Harrison (101) demonstrated from a fracture mechanics treatment of a series of uniformly spaced lack of penetration discontinuities that a ten percent interaction occurs when the discontinuities are spaced at the lesser of 2.25 times the plate thickness or 1.25 times the length of the longer discontinuity. However, from an examination of several groups of slag inclusion fatigue data, it was determined that when the spacing between the ends of individual slag inclusions is less than the thickness of the plate, or one-half the length of the longer inclusion, then multiple inclusions should be considered as one large slag inclusion (102). The overall "combined" length of interacting slag inclusions should be assumed equal to the distance between the extremities of multiple discontinuities.

The method used to obtain the rating curves for slag inclusions is similar to that used for porosity - i.e., the mean fatigue strengths of sound weldments are compared to the quality band fatigue strengths. To obtain an expression for the fatigue life as a function of the slag inclusion length, a smooth polynomial was fitted through the quality band values recommended by Harrison (99) for qualities V through X, Fig. 5.7. The curve was terminated at a level midway between qualities X and Y since there was no restriction on slag length for Y-quality. The scaling factors shown in Fig. 5.7 dictate the positions of log S-log N curves parallel to the quality bands as a function of the slag inclusion length.
Comparisons between the fatigue strengths from the quality band and sound weldment curves at 100,000 and 2,000,000 cycles are shown in Figs. 5.8 and 5.9. These curves relate the mean fatigue strength of a weldment containing a particular slag inclusion length to that of a sound weldment.

5.4 Planar Weld Discontinuities

A planar weld discontinuity is a two-dimensional interruption in the continuity of a weldment. Lack of penetration, incomplete fusion, cracks, and undercut are examples of welding discontinuities which lie in a single plane. The fatigue strength of weldments containing these types of discontinuities are examined in this section.

5.4.1 Undercut

Weld undercut is a gap or furrow located at the toe of a weld. Undercutting occurs when the weld melt does not completely fill the gap at the surface of the groove to form a smooth junction at the weld toe. Factors such as excessive welding speeds, high weld bead convexity, and weld melt with a high surface tension are commonly responsible for undercut formation (93). Deep undercutting can usually be detected visually; other methods such as dye penetrant and magnetic particle can be used to detect small undercut zones.
The depth and orientation of the undercut with respect to the applied load are the primary variables influencing the static and fatigue strengths. Critical undercut sizes for static loads can be determined using fracture mechanics to relate the undercut depth, the maximum applied stress, and the notch toughness of the material. Undercut sizes can be related also to the fatigue strength by using the initiation and propagation models outlined in Chapter 2.

The value of the theoretical stress concentration factor at the notch (undercut) is needed in order to determine the fatigue crack initiation life according to the model presented in Chapter 2. Tupek (103) conducted a finite element study of the stress concentration at the toe of transverse load-carrying fillet welds for various undercut profiles. The stress concentration factor for an axially loaded attachment was found to depend on the depth and radius of the undercut, and is given by:

\[
K_F = 1 + 2.9 \left( \frac{a_u}{r_N} \right)^{0.56}
\]  

(5.1)

where

- \(a_u\) = undercut depth,
- \(r_N\) = notch root radius of the undercut.

The value of the fatigue notch factor, \(K_F\), can be estimated by substituting Eq. 5.1 into Peterson's Equation, Eq. 2.4. The maximum value of \(K_F\) is determined by differentiating \(K_F\) with respect to the notch root radius, equating the expression to zero, and solving for the critical notch root radius. It can be shown that the maximum value
occurs at a notch root radius equal to 79 percent of the material constant $a^*$ in Peterson's Equation, and is equal to:

$$K_{f\text{max}} = 1 + 1.28 \left( \frac{a_u}{0.79 a^*} \right)^{0.56}$$  \hspace{1cm} (5.2)

The maximum fatigue notch factor is used to compute the fatigue crack initiation life for a given undercut depth.

The fatigue crack initiation lives of weldments with various undercut depths were computed at several stress levels using the initiation model described in Section 2.2.2. The fatigue strengths at desired initiation lives for weldments containing particular undercut depths were determined by log-linear interpolation between computed initiation lives.

The fatigue crack propagation life is computed from the range in stress-intensity factor and the crack growth rate characteristics of the material. The stress-intensity factor for a single edge crack in a finite width strip was assumed applicable. This assumption is valid for weld undercut positioned at several locations along the axis of the weld. Crack propagation rates of the base metal were used since a fatigue crack, initiated at a weld undercut, would propagate mostly through base metal material. The fatigue crack propagation lives were computed using Eq. 2.22 for various undercut depth and stress range combinations.

A plate thickness of 0.75-in. (19.1 mm) was assumed for the
propagation computations. This value was selected since it is a commonly used plate thickness for experimental investigations and would, therefore, provide a basis of comparison with test results. Fatigue cracks were assumed to propagate to a depth equal to thirty percent of the plate thickness before fracture.

The fatigue crack initiation and propagation lives of both mild steel and martensitic steel weldments were computed for a number of undercut depths. The total computed fatigue lives were obtained by adding the initiation and propagation estimates for similar undercut depths and stress ranges. A large number of undercut depth and stress range combinations were obtained so that log-linear interpolation could be used to determine the fatigue strength for a particular undercut depth and fatigue life.

The percentage of the total fatigue life spent in crack initiation varies depending on the material, notch severity, and magnitude of applied stress. Mattos (40) suggested a method for comparing the portion of predicted total fatigue life spent in crack initiation as a function of the predicted total fatigue life for cracks initiating at the toe of a weld. This type of comparison for cracks initiating at weld undercutting is shown in Fig. 5.10. From this figure it can be observed that a greater portion of the predicted fatigue life is spent in crack initiation for quenched and tempered steels than for mild steels at long lives, and that the initiation life is more sensitive to undercutting in quenched and tempered steel welds.
Only a limited number of experimental programs have been conducted to determine the effect of undercutting on the fatigue strength (92,104,105). Comparisons between the predicted fatigue behavior at various undercut depths and experimental data for mild steel welds containing known undercut depths are shown in Fig. 5.11. The predicted fatigue behavior is less than the experimental fatigue strengths, indicating the conservative nature of the undercut fatigue models as a result of assuming the maximum fatigue notch factor and a continuous weld undercut.

The computed fatigue strengths for welds with various undercut depths are compared at 100,000 and 2,000,000 cycles to the fatigue strength of a sound weldment in Figs. 5.12 and 5.13. It is evident from these curves that the fatigue strength is markedly reduced for small undercut depths.

5.4.2 Lack of Penetration

Lack of penetration (LOP) may be defined as an insufficient penetration of weld passes into the weld groove, thereby leaving an unwelded portion in the welded joint. Usually, lack of penetration is the result of a joint design containing an improperly sized land or gap which does not permit complete weld penetration.

A great deal of attention has been directed towards lack of
penetration discontinuities, and a number of fatigue classification methods for welds containing LOP have been suggested. For example, Harrison (101) has proposed an expression, based on fatigue crack propagation, describing the fatigue strength of weldments containing lack of penetration discontinuities. In another study, Harrison et al. (106) used the quality band approach to relate planar discontinuity size and position to the fatigue strength. Also, Tobe et al. (107) describe a method for including the fatigue crack initiation life as a part of the total computed life; the computed initiation lives were based on an approximate expression primarily applicable for plastic strain at the notch root.

The procedure for computing the fatigue crack initiation lives in this study for welds with lack of penetration discontinuities is similar to that used for welds with undercut. The theoretical stress concentration factor for a given LOP configuration must be defined. By assuming that the LOP is elliptical in shape and far enough removed from the plate surface, then the stress concentration factor is given by:

\[ K_t = 1 + 2 \left( \frac{a_{lop}}{r_N} \right)^{0.5} \]  

where \( a_{lop} \) = LOP half width,
\( r_N \) = notch root radius.

Next, the maximum fatigue notch factor must be determined. Tobe et al. (107) have shown that for an elliptical discontinuity the maximum fatigue notch factor occurs at a notch root radius equal to the material
constant, $a^*$, and is given by:

$$K_{f_{\text{max}}} = 1 + \left(\frac{a_{\text{lop}}}{a^*}\right)^{0.5} \quad (5.4)$$

Finally, the initiation life is determined for several values of the maximum fatigue notch factor, notch depth, and stress range. The fatigue crack initiation lives were computed at several arbitrarily selected LOP depths using the weld metal fatigue properties in the fatigue crack initiation model. By investigating the fatigue strength of weldments for a number of LOP depths, the fatigue strength can be estimated for any other LOP depth by interpolation.

The fatigue crack propagation lives were computed using Eq. 2.22 with crack propagation rates equal to the base metal values; as noted in Section 4.3.2, Parry et al. (50) have demonstrated that use of the base metal values are appropriate since the crack propagation rates for weld metal are not always conservative. The expression for $\Delta K$ was determined using the approximate method described in Appendix B; the $\Delta K$-expression for a crack in an infinite plate was modified by the finite width correction factor, $F_w$. It is assumed in this application that a lack of penetration discontinuity is present for the entire length of the weld. Although this is a conservative assumption, its use will safely estimate the propagation life for welds containing any lack of penetration length. The fatigue crack propagation lives were computed for stress ranges and lack of penetration depths identical to those used to compute the initiation lives.
The computed total fatigue lives were obtained by adding the computed initiation and propagation fatigue lives corresponding to each assumed LOP depth and applied stress range. A wide range in fatigue lives was obtained by selecting a large group of initial LOP depths and applied stress ranges. The predicted partitioning of the initiation life as a function of the total fatigue life is shown in Fig. 5.14. A greater portion of the fatigue life is spent in crack initiation for quenched and tempered steel welds.

Several experimental fatigue studies have been conducted for lack of penetration discontinuities in both mild steel welds (95,108,109,110) and quenched and tempered steel welds (107). Comparisons between the computed total lives for LOP in 3/4-in. (19.1 mm) welded plates and data from the studies conducted by Wilson et al. (108) and Newman et al. (109) for mild steel welds with known LOP depths are shown in Fig. 5.15. Reasonable agreement was obtained between the predicted fatigue behavior and the fatigue strengths of specimens with 0.062-in. (1.6 mm) LOP depths in 0.50-in. (12.7 mm) plates, and specimens with 0.375-in. (9.5 mm) and 0.500-in. (12.7 mm) LOP depths in 0.875-in. (22.2 mm) plates.

The fatigue strengths of welds containing lack of penetration discontinuities are compared at 100,000 and 2,000,000 cycles to the mean fatigue strength of sound welds in Fig. 5.16 and 5.17. These figures demonstrate that the fatigue strength of a weldment containing a lack of penetration is greatly reduced below that of a sound weld, especially
5.4.3 Lack of Fusion

A lack of fusion (LOF) discontinuity may be defined as a localized unfused region between the weld metal and base metal. Improper welding procedures or heavy mill scale are factors which can prevent proper weld fusion (93).

Very few experimental studies have been conducted to examine the effect of LOF on weldment fatigue strengths. Difficulties in realistically and reliably fabricating welds which intentionally contain LOF is probably the major reason so few studies have been conducted. However, the fatigue behavior of weldments with lack of fusion discontinuities can be estimated using the mathematical models for fatigue crack initiation and propagation.

The fatigue crack initiation lives of welds containing lack of fusion were assumed equal to the initiation lives of welds containing similar sized lack of penetration discontinuities. This assumption is conservative for lack of fusion oriented on planes not perpendicular to the applied loading, but is appropriate for lack of fusion oriented perpendicular to the applied loading.

The fatigue crack propagation lives were calculated on the basis of
Eq. 2.22 with crack propagation rates equal to the corresponding base metal values. The range in stress-intensity factor for an inclined crack in an infinite sheet, Fig. 5.18a, is given by Paris et al. (111) as:

$$\Delta K = \Delta S \sqrt{\pi a} \sin^2 \gamma$$  \hspace{1cm} (5.5)

where

- \(a\) = half crack depth,
- \(\gamma\) = angle between applied stress and crack.

A finite width correction factor from the approximate analysis described in Appendix B was used to correct for finite plate thickness.

The inclination angle, \(\gamma\), should be selected to suit the geometry of a particular weld groove. However, the crack propagation lives in this study were computed on the basis of an angle of 75 degrees; this angle would be appropriate for a lack of fusion discontinuity parallel to the groove of a 30 degree single-V butt weld, Fig. 5-18b. The inclination angle was intentionally selected steep to conservatively cover a wide range of possible inclination angles.

For the fatigue life calculations it was assumed that the lack of fusion was positioned at quarter-thickness. A position near mid-thickness was not selected because it would tend to behave in a manner similar to a lack of root penetration discontinuity. The fatigue crack propagation behavior at other positions, naturally, could be calculated also.
The total fatigue lives were computed by adding the computed initiation and propagation lives at selected values for the stress range and lack of fusion depth. The predicted partitioning of the initiation life as a function of the total fatigue life is shown in Fig. 5.19. From this figure it can be seen that LOF depth is not a dominant factor.

The computed fatigue strengths at 100,000 and 2,000,000 cycles are compared to the mean fatigue strengths of sound weldments in Figs. 5.20 and 5.21.

5.5 Development of Weld Quality Recommendations

5.5.1 Percent Fatigue Strength Factors and Weld Quality Levels

The curves developed in previous sections provide a direct measure of the fatigue strength in the presence of a weld discontinuity, and may be thought of as fatigue strength reduction curves. These strength reduction curves can be utilized to formulate easy-to-use correlations between weld quality and the corresponding fatigue strengths. This is accomplished by selecting three strength reduction coefficients for ranges in weld quality.

The three fatigue strength zones considered include that for no reduction in fatigue strength; that for a moderate reduction in fatigue
strength resulting from minor weld discontinuities; and that for a significant reduction in the fatigue strength resulting from severe weld discontinuities. The quantitative selection of the strength reduction factors was based on an examination of the mean fatigue strength curves from the University of Illinois fatigue data bank (73). As noted in Section 5.2, sound weldment fatigue behavior occurs at approximately one standard deviation above the best-fit curve through the data. If the best-fit curve is assumed to represent the fatigue strength curve for weldments with minor discontinuities, and the curve at one standard deviation below the best-fit curve is assumed to represent the fatigue strength curve for weldments with relatively severe discontinuities, then the reductions in fatigue strengths compared to sound weldment fatigue behavior are roughly 20 and 40 percent, respectively. Obviously, other values could be selected for comparisons with the weld quality, but the selected values are representative of the fatigue strength zones of interest.

The weld quality can now be related to the three strength levels selected above. Weld quality A represents a discontinuity severity such that there is no reduction in the mean fatigue strength of a sound weldment. Weld qualities B and C represent discontinuity severities such that there is a maximum of 20 or 40 percent reduction in the mean fatigue strength of a sound weldment. The three weld quality levels are illustrated in Fig. 5.22 for the reduction coefficients tabulated in Table 5.2. For the fatigue strength reduction method, the same reduction coefficient is used for all discontinuity severities in a
particular weld quality zone. For example, the fatigue strength for a weld with a discontinuity severity within the limits of weld quality C, shown as a dotted line in Fig. 5.22, must be taken equal to 40 percent of the fatigue strength of a sound weld, even though the reduction in fatigue strength is actually somewhere between 20 and 40 percent. This type of fatigue strength step function relationship for weld quality was used to determine the maximum discontinuity size corresponding to the prescribed reduction coefficients. These values are summarized in Tables 5.3 and 5.4 and Figs. 5.23 to 5.27.

Comparisons between the discontinuity limits in the AWS Structural Welding Code (112) and the weld quality limits corresponding to the three strength levels are shown in Figs. 5.23 to 5.27. In general the maximum discontinuity sizes corresponding to the fatigue strength reduction technique exceeds the allowable AWS limits for planar discontinuities, but not for nonplanar discontinuities. One notable exception is for undercutting in quenched and tempered steel welds, where the AWS undercut limits for buildings exceed that permitted by the strength reduction method.

The weld quality levels can be effectively used in conjunction with the fatigue strength reduction coefficients. The designer, with knowledge of the type of loading, the material, and the relative importance of each member, can best decide what weld fabrication quality level should be specified based on the reduction in mean fatigue strength which can be tolerated. The fabricator can then use the weld
quality levels tabulated in Tables 5.3 and 5.4 as a guideline to ensure that the actual weld quality satisfies that specified by the designer.

5.5.2 Recommendations

Based on an examination of the effect of weld discontinuities on the fatigue strength, it is recommended that the following criteria be used in assessing the strength of a weldment:

1. The materials used for fabrication shall be sufficiently tough so that the discontinuity sizes permitted by fatigue analyses shall not cause premature failure by brittle fracture.

2. The weld quality for a member shall be assigned on the basis of a fatigue stress less than or equal to the mean fatigue strength of a sound weldment as modified by the reduction coefficient given in Table 5.2.

3. Welds shall be fabricated so that discontinuity severities are within the limits given in Tables 5.3 or 5.4 and Figs. 5.23 to 5.27 for a specified weld quality.

4. Multiple discontinuities shall be considered as a single large discontinuity if the spacing between the ends of the discontinuities is less than or equal to the thickness of the plate or one-half the length of the longer discontinuity, $l_1$. The combined length shall be considered equal to the distance between the extremities of the discontinuities, $l_1 + \Delta l + l_2$. 
6.1 Summary

The objective of this investigation was to formulate a method for rating the effect of weld discontinuities on the fatigue behavior of weldments. To accomplish this objective mathematical fatigue models for notched members were developed, and fatigue tests of welded specimens containing artificial and actual discontinuities were conducted to evaluate the accuracy of the mathematical models.

The literature was reviewed for fatigue crack initiation and propagation models available for computing the fatigue behavior of notched or cracked members. A model for each portion of the fatigue life was selected and adapted for use in estimating the fatigue behavior of weldments containing discontinuities. The initiation model utilized Neuber control to describe the local stresses and strains, weld metal cyclic and fatigue material properties, and a linear damage summation to evaluate crack initiation lives. The propagation model utilized a numerical integration technique to solve the Paris crack growth power law to evaluate crack propagation lives.
The experimental program included fatigue tests of notched plain plate mild steel specimens, fatigue tests of mild and quenched and tempered steel butt welded specimens containing artificial discontinuities, fatigue tests of quenched and tempered transverse butt welded steel specimens containing various types of slag inclusion discontinuities, and strain relaxation tests for both short and long quenched and tempered steel welds to determine the residual stress distributions.

Fatigue data and analyses found in the literature for specimens containing known weld discontinuity severities were reviewed and compared to the computed fatigue behavior. Based on comparisons of the computed and empirically determined fatigue strengths of weldments containing various types of discontinuities, members were rated relative to the fatigue strength of sound weldments. A method is suggested for relating the weld fabrication quality to the weldment fatigue strength by partitioning the fatigue strength curves for various discontinuities into weld quality levels which correspond to selected reductions in the mean fatigue strength of a sound weldment.

6.2 Conclusions

1. Comparisons of the fatigue lives for narrow width plain plate and welded specimens indicate that, in addition to the applied stress, the notch size, through thickness position, and notch root radius all
influence the fatigue life. Plates with large, sharp discontinuities positioned near the surface have shorter fatigue lives than plates with small, blunt discontinuities positioned near mid-thickness.

2. Strain relaxation measurements of HY-80 steel butt welds using sectioning techniques indicate that: (a) the distribution of stress through the thickness varies considerably and may be unsymmetrical; (b) surface residual tensile stresses are considerably less than yield point magnitude for welds with the reinforcement removed; (c) longitudinal residual stresses increase with weld length, and can be locally as high as the yield strength of the weld metal; and (d) transverse residual stresses are compressive near mid-thickness.

3. The location of a discontinuity relative to the residual stress distribution is significant in influencing the fatigue behavior at long lives where residual stresses do not quickly relax. Fatigue crack initiation and propagation can be delayed, or even prevented, for discontinuities located in a compressive residual stress zone.

4. Fatigue test results of HY-80 transverse butt welded specimens containing slag inclusion discontinuities indicate that the slag inclusion length, size, position through the thickness, sharpness perpendicular to the applied load, and interaction with other inclusions all influence the fatigue behavior. A correlation was found to exist between fatigue strength and the percent reduction in cross-sectional area.
5. Small unintentional discontinuities, of a size less than that which can be detected by radiography, can be detrimental to the fatigue strength of a weldment and cause premature failure. Notch sharpness and location relative to the residual stress distribution are factors which determine the importance of these discontinuities.

6. The fatigue strength of weldments containing nonplanar discontinuities can be adequately described by fatigue quality bands, which relate discontinuity severities to zones separated by a family of log S-log N curves. The fatigue strength of weldments containing planar discontinuities can be adequately described by fatigue analysis for a given discontinuity size. Welds with nonplanar discontinuities appear to be suited to empirical fatigue relationships related to a reduction in the cross sectional area since the shape, sharpness, and position of nonplanar discontinuities often varies and, therefore, tends to blur the dominant discontinuity parameter. However, the effects of planar discontinuities on the fatigue strength of weldments are strongly related to discontinuity size and position.

7. A simple-to-use method for relating levels of weld quality to the reduction in mean fatigue strength of a sound weldment has been formulated from fatigue strength curves. This method provides for consistency in assessing the effect of different discontinuity types on weldment fatigue strength.
LIST OF REFERENCES


66. Bowman, M.D. and Munse, W.H., "The Effect of Slag Inclusions on


# Table 3.1

## Physical Properties of Base and Weld Metals

<table>
<thead>
<tr>
<th>Designation</th>
<th>Yield Strength (ksi)</th>
<th>Tensile Strength (ksi)</th>
<th>Elongation in 2 inches (percent)</th>
<th>Reduction in area (percent)</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mild Steel</td>
<td>29.8</td>
<td>55.1</td>
<td>34.1*</td>
<td>59.1</td>
<td>Average of three test coupons.</td>
</tr>
<tr>
<td>HY-80</td>
<td>88.5-90.0</td>
<td>108.3-109.2</td>
<td>22.6-25.3</td>
<td>69.2-70.4</td>
<td>Values reported in SRS 285 for two heats of HY-80 steel.</td>
</tr>
<tr>
<td>E70</td>
<td>68.0</td>
<td>75.0</td>
<td>34.0</td>
<td>75.0</td>
<td>Values reported by electrode manufacturer.</td>
</tr>
<tr>
<td>E110</td>
<td>103.0</td>
<td>115.0</td>
<td>22.0</td>
<td>62.0</td>
<td>Values reported by electrode manufacturer.</td>
</tr>
</tbody>
</table>

1 ksi = 6.895 MPa; 1 in. = 25.4 mm

* Percent elongation in 8-inches.
### TABLE 3.2
CHEMICAL COMPOSITIONS OF BASE AND WELD METALS

<table>
<thead>
<tr>
<th>Element</th>
<th>Mild Steel*</th>
<th>HY-80**</th>
<th>E7018+</th>
<th>E11018+</th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>0.17</td>
<td>0.16 - 0.17</td>
<td>0.06</td>
<td>0.06</td>
</tr>
<tr>
<td>Mn</td>
<td>0.44</td>
<td>0.28 - 0.33</td>
<td>1.10</td>
<td>1.53</td>
</tr>
<tr>
<td>P</td>
<td>&lt;0.005</td>
<td>0.010 - 0.021</td>
<td>0.03</td>
<td>0.030</td>
</tr>
<tr>
<td>S</td>
<td>&lt;0.030</td>
<td>0.009 - 0.019</td>
<td>0.04</td>
<td>0.030</td>
</tr>
<tr>
<td>Si</td>
<td>&lt;0.01</td>
<td>0.26 - 0.27</td>
<td>0.50</td>
<td>0.27</td>
</tr>
<tr>
<td>Cu</td>
<td>0.04</td>
<td>0.06</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Ni</td>
<td>&lt;0.01</td>
<td>2.29 - 2.86</td>
<td>-</td>
<td>1.88</td>
</tr>
<tr>
<td>Cr</td>
<td>0.02</td>
<td>1.31 - 1.61</td>
<td>-</td>
<td>0.31</td>
</tr>
<tr>
<td>Mo</td>
<td>&lt;0.01</td>
<td>0.32 - 0.48</td>
<td>0.40 - 0.65</td>
<td>0.42</td>
</tr>
<tr>
<td>Al</td>
<td>0.024</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

All compositions in weight percent.

* Results of quantitative analysis.

** Values reported in SRS 285 for two heats of steel.

+ Values reported by electrode manufacturer.
**TABLE 3.3**
FULL WIDTH
FATIGUE TESTING PROGRAM

<table>
<thead>
<tr>
<th>Series No.</th>
<th>Number of Tests</th>
<th>Fatigue Test Condition*</th>
<th>Discontinuity Condition</th>
</tr>
</thead>
<tbody>
<tr>
<td>I</td>
<td>6</td>
<td>F</td>
<td>One slag inclusion of small or large diameter, at quarter thickness, with a ( \frac{1}{2}, 1, ) or ( \frac{3}{2} )-in. slag length.</td>
</tr>
<tr>
<td>II</td>
<td>6</td>
<td>CI</td>
<td>One slag inclusion of small or large diameter, at quarter thickness, with a ( \frac{1}{2}, 1, ) or ( \frac{3}{2} )-in. slag length.</td>
</tr>
<tr>
<td>III</td>
<td>6</td>
<td>F</td>
<td>One slag inclusion of small or large diameter, at quarter thickness, with a ( \frac{1}{2}, 1, ) or ( \frac{3}{2} )-in. slag length.</td>
</tr>
<tr>
<td>IV</td>
<td>3</td>
<td>F</td>
<td>Two slag inclusions ( \frac{1}{2} )-in. apart. Each is of large diameter, at quarter thickness, and with a ( 1 )-in. slag length.</td>
</tr>
<tr>
<td>V</td>
<td>3</td>
<td>F</td>
<td>Two slag inclusions ( \frac{1}{2} )-in. apart. Each is of large diameter, at quarter thickness, with a ( 1 )-in. slag length.</td>
</tr>
<tr>
<td>VI</td>
<td>3</td>
<td>F</td>
<td>Two slag inclusion of large diameter, with a ( 1 )-in. slag length. Inclusions separated through the thickness on each side of the root passes.</td>
</tr>
<tr>
<td>VII</td>
<td>3</td>
<td>F</td>
<td>One slag inclusion of large diameter near the weld surface with a ( 1 )-in. slag length.</td>
</tr>
</tbody>
</table>

1 in. = 25.4 mm

* F Denotes fatigue cycled to complete fracture.

CI Denotes fatigue cycled to crack initiation.
### Table 4.1

**Fatigue Test Results of Notched Narrow Width Plain Plate Specimens**

<table>
<thead>
<tr>
<th>Specimen Number</th>
<th>Net Stress Range, ksi</th>
<th>Initiation Life, Cycles</th>
<th>Total Life, Cycles</th>
<th>Notch Offset, inches</th>
<th>Notch Type</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>PP-1</td>
<td>30.0</td>
<td>45,000</td>
<td>88,820</td>
<td>0.000</td>
<td>Hole</td>
<td></td>
</tr>
<tr>
<td>PP-2</td>
<td>30.0</td>
<td>4,000</td>
<td>33,350</td>
<td>0.000</td>
<td>Slot</td>
<td></td>
</tr>
<tr>
<td>PP-3</td>
<td>30.0</td>
<td>64,650</td>
<td>136,480</td>
<td>0.000</td>
<td>Hole</td>
<td>Hole formed by EDM</td>
</tr>
<tr>
<td>PP-4</td>
<td>30.0</td>
<td>3,000</td>
<td>34,540</td>
<td>0.003</td>
<td>Slot</td>
<td>Slot formed by EDM</td>
</tr>
<tr>
<td>PP-5</td>
<td>30.0</td>
<td>60,100</td>
<td>134,300</td>
<td>0.123</td>
<td>Hole</td>
<td></td>
</tr>
<tr>
<td>PP-6</td>
<td>30.0</td>
<td>3,000</td>
<td>32,360</td>
<td>0.130</td>
<td>Slot</td>
<td></td>
</tr>
<tr>
<td>PP-7</td>
<td>30.0</td>
<td>55,000</td>
<td>102,590</td>
<td>0.241</td>
<td>Hole</td>
<td></td>
</tr>
<tr>
<td>PP-8</td>
<td>30.0</td>
<td>1,000</td>
<td>25,850</td>
<td>0.256</td>
<td>Slot</td>
<td></td>
</tr>
<tr>
<td>PP-9</td>
<td>25.0</td>
<td>514,000</td>
<td>943,120</td>
<td>0.013</td>
<td>Hole</td>
<td>Overloaded at 286,000, 730,000 and 878,400 cycles</td>
</tr>
<tr>
<td>PP-10</td>
<td>25.0</td>
<td>156,750</td>
<td>356,420</td>
<td>0.242</td>
<td>Hole</td>
<td>Precycled 1,217,000 cycles at 15.5 ksi</td>
</tr>
</tbody>
</table>

1 in. = 25.4 mm; 1 ksi = 6.895 MPa

All failures occurred through the notches. Minimum stress for all tests was 0.5 ksi.
TABLE 4.2
FATIGUE TEST RESULTS - MILD STEEL WELDS

<table>
<thead>
<tr>
<th>Specimen Number and Type</th>
<th>Net Stress Range, ksi</th>
<th>Initiation Life, Cycles</th>
<th>Total Life, Cycles</th>
<th>Notch Type</th>
<th>Notch Offset, ( \psi )</th>
<th>Location* of Fracture</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>M1-SAH</td>
<td>30.0</td>
<td>-</td>
<td>2,130,000+</td>
<td>H</td>
<td>0.002</td>
<td>c</td>
<td></td>
</tr>
<tr>
<td>M2-SBH</td>
<td>30.0</td>
<td>-</td>
<td>1,988,820+</td>
<td>H</td>
<td>0.120</td>
<td>e</td>
<td></td>
</tr>
<tr>
<td>M3-SCH</td>
<td>30.0</td>
<td>-</td>
<td>2,200,000+</td>
<td>H</td>
<td>0.258</td>
<td>c</td>
<td></td>
</tr>
<tr>
<td>M4-SDH</td>
<td>30.0</td>
<td>1,460,000</td>
<td>1,801,120</td>
<td>H</td>
<td>0.333</td>
<td>b Initiation at end of slag line and pore.</td>
<td></td>
</tr>
<tr>
<td>M5-LAH</td>
<td>30.0</td>
<td>932,000</td>
<td>1,520,420</td>
<td>H</td>
<td>0.009</td>
<td>a&amp;b Initiation at small slag line near mid-width.</td>
<td></td>
</tr>
<tr>
<td>M6-LCH</td>
<td>26.0</td>
<td>647,000</td>
<td>1,341,350</td>
<td>H</td>
<td>0.253</td>
<td>a Overloaded on 3rd cycle to +40.5 ksi.</td>
<td></td>
</tr>
<tr>
<td>M7-SAS</td>
<td>30.0</td>
<td>1,896,000</td>
<td>4,001,870+</td>
<td>S</td>
<td>0.011</td>
<td>d</td>
<td></td>
</tr>
<tr>
<td>M8-SBS</td>
<td>30.0</td>
<td>-</td>
<td>2,190,000+</td>
<td>S</td>
<td>0.120</td>
<td>c</td>
<td></td>
</tr>
<tr>
<td>M9-SCS</td>
<td>30.0</td>
<td>315,000</td>
<td>476,010</td>
<td>S</td>
<td>0.243</td>
<td>a</td>
<td></td>
</tr>
<tr>
<td>M10-SDS</td>
<td>30.0</td>
<td>191,280</td>
<td>354,180</td>
<td>S</td>
<td>0.322</td>
<td>a</td>
<td></td>
</tr>
<tr>
<td>M11-LAS</td>
<td>30.0</td>
<td>568,250</td>
<td>2,013,000+</td>
<td>S</td>
<td>0.001</td>
<td>d</td>
<td></td>
</tr>
<tr>
<td>M12-LCS</td>
<td>30.0</td>
<td>31,170</td>
<td>91,060</td>
<td>S</td>
<td>0.247</td>
<td>a</td>
<td></td>
</tr>
</tbody>
</table>
TABLE 4.2 (continued)

FATIGUE TEST RESULTS - MILD STEEL WELDS

<table>
<thead>
<tr>
<th>Specimen Number and Type</th>
<th>Net Stress Range, ksi</th>
<th>Initiation Life, Cycles</th>
<th>Total Life, Cycles</th>
<th>Notch Offset, $\psi$</th>
<th>Location* of Fracture</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>M-13-LCH</td>
<td>30.0</td>
<td>435,000</td>
<td>634,520</td>
<td>H</td>
<td>0.245</td>
<td>a</td>
</tr>
</tbody>
</table>

1 in. = 25.4 mm; 1 ksi = 6.895 MPa

All tests were run with a minimum stress of 0.5 ksi.

* The location of fracture is designated by:
  a. Through the intended notch.
  b. Through a small unintentional weld discontinuity.
  c. Did not crack.
  d. Cracked at notch, but did not fail.
  e. Failure in grips, uncracked at notch.
TABLE 4.3

FATIGUE TEST RESULTS - HY-80 WELDS

<table>
<thead>
<tr>
<th>Specimen Number and Type</th>
<th>Net Stress Range, ksi</th>
<th>Initiation Life, Cycles</th>
<th>Total Life, Cycles</th>
<th>Notch Offset, $\psi$</th>
<th>Location of Failure</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>T1-SAH</td>
<td>45.0</td>
<td>1,422,660</td>
<td>1,498,840</td>
<td>H</td>
<td>0.002</td>
<td>b</td>
</tr>
<tr>
<td>T2-SBH</td>
<td>45.0</td>
<td>-</td>
<td>2,312,850+</td>
<td>H</td>
<td>0.146</td>
<td>c</td>
</tr>
<tr>
<td>T3-SCH</td>
<td>45.0</td>
<td>996,000</td>
<td>1,334,070</td>
<td>H</td>
<td>0.260</td>
<td>a</td>
</tr>
<tr>
<td>T4-SDH</td>
<td>45.0</td>
<td>365,000</td>
<td>471,960</td>
<td>H</td>
<td>0.336</td>
<td>a</td>
</tr>
<tr>
<td>T5-LAH</td>
<td>45.0</td>
<td>-</td>
<td>1,306,750+</td>
<td>H</td>
<td>0.014</td>
<td>c</td>
</tr>
<tr>
<td>T6-LCH</td>
<td>40.0</td>
<td>265,000</td>
<td>361,650</td>
<td>H</td>
<td>0.268</td>
<td>a</td>
</tr>
<tr>
<td>T7-SAS</td>
<td>45.0</td>
<td>-</td>
<td>2,022,000+</td>
<td>S</td>
<td>0.003</td>
<td>c</td>
</tr>
<tr>
<td>T8-SBS</td>
<td>45.0</td>
<td>86,800</td>
<td>120,720</td>
<td>S</td>
<td>0.132</td>
<td>a</td>
</tr>
<tr>
<td>T9-SCS</td>
<td>45.0</td>
<td>30,000</td>
<td>106,550</td>
<td>S</td>
<td>0.253</td>
<td>a</td>
</tr>
<tr>
<td>T10-SDS</td>
<td>45.0</td>
<td>45,000</td>
<td>86,060</td>
<td>S</td>
<td>0.334</td>
<td>a</td>
</tr>
<tr>
<td>T11-LAS</td>
<td>45.0</td>
<td>104,250</td>
<td>7,066,800+</td>
<td>S</td>
<td>0.009</td>
<td>d</td>
</tr>
</tbody>
</table>
Table 4.3 (continued)

FATIGUE TEST RESULTS - HY-80 WELDS

<table>
<thead>
<tr>
<th>Specimen Number and Type</th>
<th>Net Stress Range, ksi</th>
<th>Initiation Life, Cycles</th>
<th>Total Life, Cycles</th>
<th>Notch Offset, ( \psi )</th>
<th>Location of Failure</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>T12-LCS</td>
<td>45.0</td>
<td>21,000</td>
<td>44,070</td>
<td>S</td>
<td>0.251</td>
<td>a</td>
</tr>
<tr>
<td>T13-LCH</td>
<td>45.0</td>
<td>78,000</td>
<td>181,460</td>
<td>H</td>
<td>0.262</td>
<td>a</td>
</tr>
<tr>
<td>T14-LCH</td>
<td>36.0</td>
<td>894,000</td>
<td>1,016,990</td>
<td>H</td>
<td>0.266</td>
<td>b</td>
</tr>
</tbody>
</table>

Initiation at slag line along weld boundary.

1 in. - 25.4 mm; 1 ksi - 6.895 MPa

All tests were conducted with a minimum stress of 0.5 ksi.

* The location of failure is designated by:

a. Through the intended notch.

b. Through a small unintentional weld discontinuity.

c. Did not crack.

d. Cracked at notch, but did not crack.

e. Failure in grips, uncracked at notch.
# Table 4.4

**Fatigue Test Data of Full Width HY-80 Welded Specimens**

<table>
<thead>
<tr>
<th>Specimen Number</th>
<th>Gross Area of Weld, Sq. In.</th>
<th>Life to Failure, Cycles</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>NA - 1</td>
<td>5.287</td>
<td>15,330</td>
<td>Well rounded defect at point of initiation. Small unintentional defect at 7/8&quot; from centerline.</td>
</tr>
<tr>
<td>NB - 2</td>
<td>5.233</td>
<td>10,930</td>
<td>Sharp defect at point of initiation. Unintentional defect at 1-13/16&quot; from centerline.</td>
</tr>
<tr>
<td>NC - 1</td>
<td>5.232</td>
<td>21,710</td>
<td>Overloaded at 1000 cycles. Unintentional defect at 1-27/32&quot; and 1-3/4&quot; from centerline.</td>
</tr>
<tr>
<td>WA - 1</td>
<td>5.328</td>
<td>10,520</td>
<td>Large pore near surface at mid-width. Unintentional defect at 1-9/16&quot; from centerline.</td>
</tr>
<tr>
<td>WB - 4</td>
<td>5.268</td>
<td>12,060</td>
<td>A small pore visible on fracture surface, 1/4&quot; from plate surface.</td>
</tr>
<tr>
<td>WC - 5</td>
<td>5.240</td>
<td>16,950</td>
<td>Fairly large defect but well rounded.</td>
</tr>
</tbody>
</table>

**Series No. I**

**Series No. III**

| NA - 3          | 5.135                      | 18,620                  | Large but well rounded slag pocket. Small unintentional flaw at 11/16" from centerline. |
| NB - 3          | 5.113                      | 17,790                  | Middle portion of slag pocket burned out. Initiation occurred at two flat, sharp flaws. |
### TABLE 4.4 (continued)

**FATIGUE TEST DATA OF FULL WIDTH HY-80 WELDED SPECIMENS**

<table>
<thead>
<tr>
<th>Specimen Number</th>
<th>Gross Area of Weld, Sq. In.</th>
<th>Life to Failure, Cycles</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>NC - 3</td>
<td>5.222</td>
<td>18,090</td>
<td>Slag pocket discontinuous along the length. Small unintentional flaw and group of pores initiated cracks separate from defect.</td>
</tr>
<tr>
<td>WA - 3</td>
<td>5.230</td>
<td>18,850</td>
<td>Flaw widens at both ends and becomes flat. Group of small pores visible.</td>
</tr>
<tr>
<td>WB - 2</td>
<td>5.011</td>
<td>9,440</td>
<td>Slag line well rounded overall. Initiation at sharp points on defect and at small unintentional flaw.</td>
</tr>
<tr>
<td>WC - 1</td>
<td>4.982</td>
<td>18,180</td>
<td>Deep well rounded defect. Group of small pores near the surface on the opposite side of the defect.</td>
</tr>
</tbody>
</table>

**Series No. IV**

<table>
<thead>
<tr>
<th>Specimen Number</th>
<th>Gross Area of Weld, Sq. In.</th>
<th>Life to Failure, Cycles</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>MA - 1</td>
<td>5.409</td>
<td>3,280</td>
<td>Two large and deep defects that flatten near the edges. Unintentional flaw between defects initiated a crack on a different plane.</td>
</tr>
<tr>
<td>MA - 2</td>
<td>5.413</td>
<td>2,800</td>
<td>Two large defects, one of them flat in some spots. Cracks initiated at flat defect and unintentional flaw near the specimen edge.</td>
</tr>
<tr>
<td>MA - 3</td>
<td>5.405</td>
<td>3,240</td>
<td>Two deep, rectangular defects. Small unintentional flaw between the defects.</td>
</tr>
<tr>
<td>Specimen Number</td>
<td>Gross Area of Weld, Sq. In.</td>
<td>Life to Failure, Cycles</td>
<td>Remarks</td>
</tr>
<tr>
<td>-----------------</td>
<td>-----------------------------</td>
<td>-------------------------</td>
<td>---------</td>
</tr>
<tr>
<td>MB - 1</td>
<td>5.395</td>
<td>5,130</td>
<td>Two large well rounded defects. Small unintentional flaw between the two defects.</td>
</tr>
<tr>
<td>MB - 2</td>
<td>5.326</td>
<td>6,620</td>
<td>Two deep defects with small unintentional flaw between them. Initiation at points where defects flare out.</td>
</tr>
<tr>
<td>MB - 3</td>
<td>5.261</td>
<td>4,170</td>
<td>Two large rectangular defects. Initiation at flat flares on defect and at small unintentional flaw.</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>Series No. V</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>LA - 1</td>
<td>5.254</td>
<td>5,480</td>
<td>Two large well rounded defects. Small unintentional flaws at 1-3/8&quot;, 1-1/2&quot;, and 1-3/4&quot; from the centerline. Initiation at a sharp point on one of the defects and at the unintentional flaws.</td>
</tr>
<tr>
<td>LA - 2</td>
<td>5.348</td>
<td>6,490</td>
<td>Two deep flaws, one of which is rectangular and the other well rounded. Small pores visible near both surfaces.</td>
</tr>
<tr>
<td>LA - 3</td>
<td>5.280</td>
<td>6,950</td>
<td>Two large well rounded defects. Very narrow but deep unintentional flaw at mid-thickness. Initiation around another unintentional defect located 1-1/4&quot; from the centerline.</td>
</tr>
</tbody>
</table>
### TABLE 4.4 (continued)

**FATIGUE TEST DATA OF FULL WIDTH HY-80 WELDED SPECIMENS**

<table>
<thead>
<tr>
<th>Specimen Number</th>
<th>Gross Area of Weld, Sq. In.</th>
<th>Life to Failure, Cycles</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>LB - 1</td>
<td>5.363</td>
<td>15,800</td>
<td>Deep rectangular flaw near the surface. A small pore of 1/8&quot; diameter is visible at about mid-thickness and 1/2&quot; from the centerline.</td>
</tr>
<tr>
<td>LB - 2</td>
<td>5.322</td>
<td>12,960</td>
<td>Deep well rounded flaw near the surface of the plate. A small pore is present close to mid-thickness and at 5/16&quot; from the centerline. Initiation at an unintentional defect 2&quot; from the centerline near the surface.</td>
</tr>
<tr>
<td>LB - 3</td>
<td>5.298</td>
<td>18,850</td>
<td>Defect of rectangular shape close to the surface. Two small pores can be seen between the flaw and the plate surface. Two small unintentional flaws near the weld surface where initiation is evident.</td>
</tr>
</tbody>
</table>

1 in = 25.4 mm

All failures were located in the weld metal.

Stress cycle on gross area was 0.50 ksi to 60.5 ksi tension.
<table>
<thead>
<tr>
<th>Series No.</th>
<th>Specimen Number</th>
<th>( N_I ) Estimate Using Strain Gages, Cycles</th>
<th>( N_I ) Estimate Using Ultrasonic Testing, Cycles</th>
<th>( (N_I)_{\text{min}}/N_T )%</th>
<th>( N_{sc}^* ) Cycles</th>
<th>( N_I - N_{sc}^* ) Cycles</th>
<th>( N_{sc}^*/N_T )%</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>NA-1</td>
<td>2,000</td>
<td>2,000</td>
<td>13.1</td>
<td>5,500</td>
<td>3,500</td>
<td>35.9</td>
</tr>
<tr>
<td></td>
<td>NB-2</td>
<td>2,000</td>
<td>2,000</td>
<td>18.3</td>
<td>6,000</td>
<td>4,000</td>
<td>54.9</td>
</tr>
<tr>
<td></td>
<td>NC-1</td>
<td>12,000</td>
<td>4,500</td>
<td>20.7</td>
<td>16,000</td>
<td>11,500</td>
<td>73.7</td>
</tr>
<tr>
<td></td>
<td>WA-1</td>
<td>2,000</td>
<td>2,000</td>
<td>19.0</td>
<td>6,000</td>
<td>4,000</td>
<td>57.0</td>
</tr>
<tr>
<td></td>
<td>WB-4</td>
<td>4,000</td>
<td>3,000</td>
<td>24.9</td>
<td>6,500</td>
<td>3,500</td>
<td>53.9</td>
</tr>
<tr>
<td></td>
<td>WC-5</td>
<td>6,000</td>
<td>6,000</td>
<td>35.4</td>
<td>10,000</td>
<td>4,000</td>
<td>59.0</td>
</tr>
<tr>
<td>III</td>
<td>NA-3</td>
<td>10,000</td>
<td>5,000</td>
<td>26.9</td>
<td>10,330</td>
<td>5,330</td>
<td>55.5</td>
</tr>
<tr>
<td></td>
<td>NB-3</td>
<td>6,000</td>
<td>4,000</td>
<td>22.5</td>
<td>6,150</td>
<td>2,150</td>
<td>35.6</td>
</tr>
<tr>
<td></td>
<td>NC-3</td>
<td>9,000</td>
<td>5,000</td>
<td>27.6</td>
<td>10,000</td>
<td>5,000</td>
<td>55.3</td>
</tr>
<tr>
<td></td>
<td>WA-3</td>
<td>5,000</td>
<td>6,000</td>
<td>26.5</td>
<td>10,000</td>
<td>5,000</td>
<td>53.1</td>
</tr>
<tr>
<td></td>
<td>WB-2</td>
<td>4,000</td>
<td>3,000</td>
<td>31.8</td>
<td>4,560</td>
<td>1,560</td>
<td>48.3</td>
</tr>
<tr>
<td></td>
<td>WC-1</td>
<td>8,000</td>
<td>7,000</td>
<td>38.5</td>
<td>8,500</td>
<td>1,500</td>
<td>46.8</td>
</tr>
</tbody>
</table>
### TABLE 4.5 (continued)

ESTIMATED FATIGUE CRACK INITIATION LIFE AND OBSERVED STRESS CYCLES FOR A SURFACE CRACK

<table>
<thead>
<tr>
<th>Series No.</th>
<th>Specimen Number</th>
<th>$N_I$ Estimate Using Strain Gages, Cycles</th>
<th>$N_I$ Estimate Using Ultrasonic Testing, Cycles</th>
<th>$(N_I/_{\text{min}}/N_t)$%</th>
<th>$N_{sc}^*$</th>
<th>$N_I - N_{sc}$ Cycles</th>
<th>$N_{sc}/N_T^*$%</th>
</tr>
</thead>
<tbody>
<tr>
<td>IV</td>
<td>MA-1</td>
<td>2,000</td>
<td>1,000</td>
<td>30.5</td>
<td>3,000</td>
<td>2,000</td>
<td>91.5</td>
</tr>
<tr>
<td></td>
<td>MA-2</td>
<td>1,000</td>
<td>1,500</td>
<td>35.7</td>
<td>2,000</td>
<td>1,000</td>
<td>71.4</td>
</tr>
<tr>
<td></td>
<td>MA-3</td>
<td>1,000</td>
<td>500</td>
<td>15.4</td>
<td>2,200</td>
<td>1,700</td>
<td>67.9</td>
</tr>
<tr>
<td>V</td>
<td>MB-1</td>
<td>1,500</td>
<td>1,000</td>
<td>19.5</td>
<td>3,000</td>
<td>2,000</td>
<td>58.5</td>
</tr>
<tr>
<td></td>
<td>MB-2</td>
<td>1,500</td>
<td>500</td>
<td>7.6</td>
<td>3,000</td>
<td>2,500</td>
<td>45.3</td>
</tr>
<tr>
<td></td>
<td>MB-3</td>
<td>500</td>
<td>500</td>
<td>12.0</td>
<td>2,250</td>
<td>1,750</td>
<td>54.0</td>
</tr>
<tr>
<td>VI</td>
<td>LA-1</td>
<td>1,250</td>
<td>1,250</td>
<td>22.8</td>
<td>3,500</td>
<td>2,250</td>
<td>63.9</td>
</tr>
<tr>
<td></td>
<td>LA-2</td>
<td>1,500</td>
<td>1,500</td>
<td>23.1</td>
<td>3,500</td>
<td>2,000</td>
<td>53.9</td>
</tr>
<tr>
<td></td>
<td>LA-3</td>
<td>1,500</td>
<td>1,500</td>
<td>21.6</td>
<td>4,000</td>
<td>2,500</td>
<td>57.6</td>
</tr>
<tr>
<td>VII</td>
<td>LB-1</td>
<td>2,000</td>
<td>1,500</td>
<td>9.5</td>
<td>10,750</td>
<td>9,250</td>
<td>68.0</td>
</tr>
<tr>
<td></td>
<td>LB-2</td>
<td>2,000</td>
<td>500</td>
<td>3.9</td>
<td>2,500</td>
<td>2,000</td>
<td>19.3</td>
</tr>
<tr>
<td></td>
<td>LB-3</td>
<td>-</td>
<td>1,500</td>
<td>8.0</td>
<td>7,900</td>
<td>6,400</td>
<td>41.9</td>
</tr>
</tbody>
</table>

* $N_{sc}$ = Number cycles before visual detection of a surface crack.
<table>
<thead>
<tr>
<th>Quality Level</th>
<th>V</th>
<th>W</th>
<th>X</th>
<th>Y</th>
<th>Z</th>
</tr>
</thead>
<tbody>
<tr>
<td>Max. Volume of Porosity (Percent)</td>
<td>0</td>
<td>3</td>
<td>8</td>
<td>20</td>
<td>20</td>
</tr>
<tr>
<td>Max. Slag Inclusion Length (mm)</td>
<td>0</td>
<td>1.5</td>
<td>10.0</td>
<td>NM</td>
<td>NM</td>
</tr>
</tbody>
</table>

1 in. = 25.4 mm

NM refers to no maximum length.

Values recommended in Refs. (94) and (99).
### TABLE 5.2 FATIGUE STRENGTH REDUCTION COEFFICIENTS

<table>
<thead>
<tr>
<th>Weld Quality</th>
<th>A</th>
<th>B</th>
<th>C</th>
</tr>
</thead>
<tbody>
<tr>
<td>$F_D^*$</td>
<td>1.00</td>
<td>0.80</td>
<td>0.60</td>
</tr>
</tbody>
</table>

$F_D^*$ = Reduction coefficient used to reduce the mean fatigue strength of a sound weldment to give a fatigue strength which corresponds to the specified weld quality.
TABLE 5.3 WELD QUALITIES FOR VARIOUS DISCONTINUITIES (MILD STEEL)

(Based on fatigue behavior of butt welds in 1 in. plates)

<table>
<thead>
<tr>
<th>Discontinuity Type</th>
<th>Maximum Values of Discontinuities</th>
<th>Weld Quality Classification</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>A</td>
</tr>
<tr>
<td>Undercut (depth)</td>
<td>$10^5$</td>
<td>N.O</td>
</tr>
<tr>
<td></td>
<td>$2(10)^6$</td>
<td>N.O</td>
</tr>
<tr>
<td>Lack of Penetration (depth)</td>
<td>$10^5$</td>
<td>1/64</td>
</tr>
<tr>
<td></td>
<td>$2.10^6$</td>
<td>1/64</td>
</tr>
<tr>
<td>Lack of Fusion (depth)</td>
<td>$10^5$</td>
<td>1/32</td>
</tr>
<tr>
<td></td>
<td>$2.10^6$</td>
<td>1/64</td>
</tr>
<tr>
<td>Slag Inclusion (length)</td>
<td>$10^5$</td>
<td>1/64</td>
</tr>
<tr>
<td></td>
<td>$2.10^6$</td>
<td>N.O.</td>
</tr>
<tr>
<td>Porosity (percentage)</td>
<td>$10^5$</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$2.10^6$</td>
<td>N.O.</td>
</tr>
</tbody>
</table>

1 in. = 25.4 mm.

The slag inclusion length and undercut, lack of penetration, and lack of fusion depths are in inches. The porosity values are in percent volume of the weld. N.O. denotes a non-observable discontinuity size.
TABLE 5.4 WELD QUALITIES FOR VARIOUS DISCONTINUITIES (QUENCHED & TEMPERED STEEL)

(Based on fatigue behavior of butt welds in 1 in. plates)

<table>
<thead>
<tr>
<th>Discontinuity Type</th>
<th>Weld Quality Classification</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>A</td>
</tr>
<tr>
<td>Undercut (depth)</td>
<td>$10^5$</td>
</tr>
<tr>
<td></td>
<td>$2.10^6$</td>
</tr>
<tr>
<td>Lack of Penetration (depth)</td>
<td>$10^5$</td>
</tr>
<tr>
<td></td>
<td>$2.10^6$</td>
</tr>
<tr>
<td>Lack of Fusion (depth)</td>
<td>$10^6$</td>
</tr>
<tr>
<td></td>
<td>$2.10^6$</td>
</tr>
<tr>
<td>Slag Inclusion (length)</td>
<td>$10^5$</td>
</tr>
<tr>
<td></td>
<td>$2.10^6$</td>
</tr>
<tr>
<td>Porosity (percentage)</td>
<td>$10^5$</td>
</tr>
<tr>
<td></td>
<td>$2.10^6$</td>
</tr>
</tbody>
</table>

1 in. = 25.4 mm.

The slag inclusion length and undercut, lack of penetration, and lack of fusion depths are in inches. The porosity values are in percent volume of the weld. N.O. denotes a non-observable discontinuity size.
Weld Toe Geometry

(a) Geometrical Discontinuities

(b) Internal Discontinuities

FIG. 1.1 COMMON GEOMETRICAL AND INTERNAL WELD DISCONTINUITIES.
FIG. 2.1 REMOTE AND LOCAL STRESSES AND STRAINS.
FIG. 2.2 REPRESENTATIVE MONOTONIC AND CYCLIC STRESS-STRAIN CURVES.

Cyclic Curve
\[ \frac{\Delta \varepsilon}{2} = \frac{\Delta \sigma}{2E} + \left(\frac{\Delta \sigma}{2K'}\right)^{V_{n'}} \]

Monotonic Curve
\[ \varepsilon = \frac{\sigma}{E} + \left(\frac{\sigma}{K}\right)^{V_{n'}} \]
FIG. 2.3 FACTORS INFLUENCING CYCLIC STRESS-STRAIN RESPONSE.
FIG. 2.4 STRAIN AMPLITUDE VS. REVERSALS TO FAILURE.
FIG. 2.5 FLOW CHART SHOWING COMBINATION OF REQUIREMENTS FOR FATIGUE CRACK INITIATION ANALYSIS.
FIG. 2.6 MECHANICAL MODEL OF STRESS-STRAIN BEHAVIOR (REF. 35).
FIG. 2.7 SCHEMATIC DIAGRAM OF FATIGUE CRACK GROWTH BEHAVIOR IN STEELS.
3/16" (4.76 mm) Thick Mild Steel Specimen With Notch Location, e. (Specimen Cut From 1 in. Thick Plate.)

Holes Drilled Undersize And Reamed. Slots Formed With No. 65 (0.035-in. dia.) Hole And Jeweler's Sawcut.

A — No Offset
B — 1/4" (6.35 mm) Offset
C — 1/2" (12.70 mm) Offset

FIG. 3.1 PLAIN PLATE SPECIMEN DIMENSIONS AND NOTCH SHAPES AND LOCATIONS.
### Mild Steel Weld

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>5/32</td>
<td>140-160</td>
<td>1</td>
<td>5/32</td>
<td>120-130</td>
</tr>
<tr>
<td>2</td>
<td>5/32</td>
<td>150-160</td>
<td>2</td>
<td>5/32</td>
<td>160-170</td>
</tr>
<tr>
<td>3-12</td>
<td>3/16</td>
<td>220-240</td>
<td>3-12</td>
<td>3/16</td>
<td>220-230</td>
</tr>
</tbody>
</table>

Electrode: E7018  
Preheat: None  

Voltage: 22-24 Volts  
Polarity: D.C. Reversed  
Interpass Temperature: 200°F (93°C) Maximum  
All Welding in Flat Position  
Backside of Pass 1 Ground Before Placing Pass 2

### HY-80 Weld

<table>
<thead>
<tr>
<th>Pass</th>
<th>Electrode Size, in.</th>
<th>Current, amps</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>5/32</td>
<td>120-130</td>
</tr>
<tr>
<td>2</td>
<td>5/32</td>
<td>160-170</td>
</tr>
<tr>
<td>3-12</td>
<td>3/16</td>
<td>220-230</td>
</tr>
</tbody>
</table>

Electrode: E11018  
Preheat: 200°F (93°C)  

FIG. 3.2 WELDING PROCEDURES FOR MILD STEEL AND HY-80 STEEL PLATES.
Mild Steel (E7018 Electrodes)  
HY-80 Steel (E11018 Electrodes)  
e = Notch Location  
3/16" (4.76 mm) Thickness

(a) Specimen Dimensions

Holes Drilled Undersize and Reamed  
Slots made From No. 65 (0.035-in. dia.) Drilled Hole With Jeweler's  
Saw Cut On Each Side

(b) Notch Shapes

Section Free Of Weld Discontinuities

(c) Notch Locations

Distance To Right Edge
A - 0.500 in. (12.7 mm)  
B - 0.375 in. (9.53 mm)  
C - 0.250 in. (6.35 mm)  
D - 0.175 in. (4.44 mm)

(d) Specimen Sectioning

FIG. 3.3 WELDED SPECIMEN DIMENSIONS AND NOTCH SHAPES AND LOCATIONS.
(a) 50 kip Fatigue Testing Machine.

(b) Traveling Microscope with Dial Gages.

FIG. 3.4 FATIGUE TESTING MACHINE WITH TRAVELING MICROSCOPE ATTACHED.
<table>
<thead>
<tr>
<th>Large Diameter Slag</th>
<th>Small Diameter Slag</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>5/32</td>
</tr>
<tr>
<td>2</td>
<td>5/32</td>
</tr>
<tr>
<td>3</td>
<td>3/16</td>
</tr>
<tr>
<td>4, 5</td>
<td>3/16</td>
</tr>
<tr>
<td>6</td>
<td>5/32</td>
</tr>
<tr>
<td>7-14</td>
<td>3/16</td>
</tr>
</tbody>
</table>

Voltage: 22-24 Volts  
Polarity: D.C. Reversed  
Preheat: 200°F (93°C)  
Electrode: E11018  
Interpass Temperature: 200°F (93°C) Maximum  
All Welding in Flat Position  
Backside of Pass 1 Ground Before Placing Pass 2

(a) Welding Procedures for Series I - V

FIG. 3.5 WELDING PROCEDURE FOR LARGE SCALE SPECIMENS.
<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1/8</td>
<td>90-110</td>
<td>1</td>
<td>5/32</td>
<td>140-160</td>
</tr>
<tr>
<td>2</td>
<td>1/8</td>
<td>130-150</td>
<td>2</td>
<td>5/32</td>
<td>160-180</td>
</tr>
<tr>
<td>3-5</td>
<td>1/8</td>
<td>100-120</td>
<td>3</td>
<td>5/32</td>
<td>160-180</td>
</tr>
<tr>
<td>6</td>
<td>5/32</td>
<td>130-150</td>
<td>4-8</td>
<td>3/16</td>
<td>200-240</td>
</tr>
<tr>
<td>7-9</td>
<td>1/8</td>
<td>90-110</td>
<td>9-11</td>
<td>1/8</td>
<td>90-110</td>
</tr>
<tr>
<td>10</td>
<td>5/32</td>
<td>130-150</td>
<td>12</td>
<td>5/32</td>
<td>130-150</td>
</tr>
</tbody>
</table>

Voltage: 22-24 Volts  
Polarity: D.C. Reversed  
Preheat: 200° F (93° C)  
Electrode: E11018  
Interpass Temperature: 200° F (93° C) Maximum  
All Welding in Flat Position  
Backside of Pass 1 Ground Before Placing Pass 2

(b) Welding Procedure for Series VI - VII

FIG. 3.5 (continued) WELDING PROCEDURE FOR LARGE SCALE SPECIMENS.
Note: Specimen Blanks Were Cut From Welded Plate After Locating Defects

Section A-A

Note: Weld Reinforcement Machined Off

FIG. 3.6 WELD PLATE GROOVE AND SPECIMEN DIMENSIONS.
FIG. 3.7 TESTING FRAME OF 400 KIP FATIGUE TESTING MACHINE.
FIG. 4.1  FATIGUE TEST RESULTS FOR MILD STEEL PLAIN PLATE SPECIMENS WITH NOTCHES OR HOLES.
FIG. 4.2 COMPARISON OF COMPUTED AND OBSERVED FATIGUE CRACK INITIATION AND PROPAGATION LIVES FOR MILD STEEL PLAIN PLATE SPECIMENS.
FIG. 4.3 - CRACK WIDTH MEASURED AFTER INCREMENTS OF VARIOUS APPLIED LOADING CYCLES FOR SPECIMEN PP-2.
FIG 4.4 SUMMARY OF FATIGUE CRACK GROWTH DATA FOR CENTRALLY NOTCHED MILD STEEL SPECIMENS.
FIG. 4.5 COMPARISON OF COMPUTED AND OBSERVED FATIGUE LIVES FOR MILD STEEL PLAIN PLATE SPECIMENS.
FIG. 4.6 FATIGUE TEST RESULTS FOR NOTCHED MILD STEEL WELDED SPECIMENS.
FIG. 4.7 FATIGUE TEST RESULTS FOR NOTCHED HY-80 STEEL WELDED SPECIMENS.
FIG. 4.8 FRACTURE SURFACES OF MILD STEEL SPECIMENS WITH CRACK INITIATION AT UNINTENTIONAL DISCONTINUITIES.
FIG. 4.9 FRACTURE SURFACES OF HY-80 STEEL WELDED SPECIMENS WITH CRACK INITIATION AT UNINTENTIONAL DISCONTINUITIES.
FIG. 4.10 REGIONS OF SPECIMEN M-6 EXAMINED WITH SCANNING ELECTRON MICROSCOPE.
FIG. 4.11 RESULTS OF VICKERS PYRAMID HARDNESS (200 GM. LOAD) SURVEY 10.9 MM BELOW PLATE SURFACE FOR SPECIMEN M-11.
FIG. 4.12 RESULTS OF VICKERS PYRAMID HARDNESS (200 GM. LOAD) SURVEY 11.91 MM BELOW PLATE SURFACE FOR SPECIMEN T-11.
FIG. 4.13 COMPARISON OF COMPUTED AND OBSERVED FATIGUE CRACK INITIATION LIVES FOR NARROW WIDTH WELDED SPECIMENS.
FIG. 4.14 COMPARISON OF COMPUTED AND OBSERVED FATIGUE CRACK PROPAGATION LIVES FOR NARROW WIDTH WELDED SPECIMENS.
FIG. 4.15 COMPARISON OF COMPUTED AND OBSERVED FATIGUE LIVES FOR NARROW WIDTH WELDED SPECIMENS.
FIG. 4.16 FATIGUE TEST RESULTS FOR WELDED HY-80 STEEL SPECIMENS CONTAINING SLAG INCLUSION DISCONTINUITIES.
FIG. 4.17 COMPUTED FATIGUE STRENGTH AT 100,000 CYCLES FOR SECTION AREA REDUCED BY SLAG INCLUSION DISCONTINUITIES.
Distance Of Crack From Plate Edge, mm

FIG. 4.18 EDGE STRAIN RANGE FOR VARIOUS MEASURED CRACK POSITIONS RELATIVE TO THE SPECIMEN EDGE.
FIG. 4.19 COMPARISON OF COMPUTED AND OBSERVED FATIGUE CRACK INITIATION AND PROPAGATION LIVES FOR FULL WIDTH WELDED SPECIMENS.
FIG. 4.20 COMPARISON OF COMPUTED AND OBSERVED FATIGUE LIVES FOR FULL WIDTH WELDED SPECIMENS.
FIG. 5.1 COMPARISON OF WELD DISCONTINUITY FATIGUE DATA TO MEAN FATIGUE CURVE.
FIG. 5.2 COMPARISON OF FATIGUE TEST DATA AND QUALITY BAND APPROACH FOR POROSITY.
FIG. 5.3 VOLUMES CIRCUMSCRIBING POROSITY CLUSTERS FOR BUTT WELDED MEMBERS AS INTERPRETED FROM A RADIOGRAPH.
FIG. 5.4 LOG S - LOG N SCALING FACTOR AS A FUNCTION OF THE PERCENT VOLUME OF POROSITY.
FIG. 5.5 FATIGUE STRENGTH OF A WELDMENT CONTAINING POROSITY AS A PERCENTAGE OF THE MEAN FATIGUE STRENGTH OF A SOUND MILD STEEL WELD.
FIG. 5.6 FATIGUE STRENGTH OF A WELDMENT CONTAINING POROSITY AS A PERCENTAGE OF THE MEAN FATIGUE STRENGTH OF A SOUND MARTENSITIC STEEL WELD.
FIG. 5.7 LOG S - LOG N SCALING FACTOR AS A FUNCTION OF THE SLAG INCLUSION LENGTH.
FIG. 5.8 FATIGUE STRENGTH OF A WELDMENT CONTAINING SLAG INCLUSIONS AS A PERCENTAGE OF THE MEAN FATIGUE STRENGTH OF A SOUND MILD STEEL WELD.
FIG. 5.9 FATIGUE STRENGTH OF A WELDMENT CONTAINING SLAG INCLUSIONS AS A PERCENTAGE OF THE MEAN FATIGUE STRENGTH OF A SOUND MARTENSITIC STEEL WELD.
FIG. 5.10 PREDICTED PARTITIONING OF FATIGUE CRACK INITIATION LIFE AS A FUNCTION OF THE TOTAL FATIGUE LIFE FOR MILD STEEL AND QUENCHED AND TEMPERED STEEL WELDS WITH UNDERCUTTING.
FIG. 5.11  COMPARISON OF COMPUTED AND EXPERIMENTAL FATIGUE BEHAVIOR FOR WELDS CONTAINING UNDERCUT.
FIG. 5.12  FATIGUE STRENGTH OF A WELDMENT CONTAINING UNDERCUT AS A PERCENTAGE OF THE MEAN FATIGUE STRENGTH OF A SOUND MILD STEEL WELD.
FIG. 5.13 FATIGUE STRENGTH OF A WELDMENT CONTAINING UNDERCUT AS A PERCENTAGE OF THE MEAN FATIGUE STRENGTH OF A SOUND MARTENSITIC STEEL WELD.
FIG. 5.14 PREDICTED PARTITIONING OF FATIGUE CRACK INITIATION LIFE AS A FUNCTION OF THE TOTAL FATIGUE LIFE FOR MILD STEEL AND QUENCHED AND TEMPERED STEEL WELDS WITH LACK OF PENETRATION.
FIG. 5.15 COMPARISON OF COMPUTED AND EXPERIMENTAL FATIGUE BEHAVIOR FOR WELDS CONTAINING LACK OF PENETRATION.
FIG. 5.16 FATIGUE STRENGTH OF A WELDMENT CONTAINING LACK OF PENETRATION AS A PERCENTAGE OF THE MEAN FATIGUE STRENGTH OF A SOUND MILD STEEL WELD.
FIG. 5.17 FATIGUE STRENGTH OF A WELDMENT CONTAINING LACK OF PENETRATION AS A PERCENTAGE OF THE MEAN FATIGUE STRENGTH OF A SOUND MARTENSITIC STEEL WELD.
\[ \Delta K = \Delta S \sqrt{\pi a} \sin^2 \gamma \]

(a) Stress-Intensity Factor for an Inclined Crack in an Infinite Sheet.

(b) Lack of Fusion Discontinuity Oriented at 75 Degrees to the Applied Load in a Single-V Butt Weld.
FIG. 5.19 PREDICTED PARTITIONING OF FATIGUE CRACK INITIATION LIFE AS A FUNCTION OF THE TOTAL FATIGUE LIFE FOR MILD STEEL AND QUENCHED AND TEMPERED STEEL WELDS WITH LACK OF FUSION.
FIG. 5.20  FATIGUE STRENGTH OF A WELDMENT CONTAINING LACK OF FUSION AS A PERCENTAGE OF THE MEAN FATIGUE STRENGTH OF A SOUND MILD STEEL WELD.
FIG. 5.21  FATIGUE STRENGTH OF A WELDMENT CONTAINING LACK OF FUSION AS A PERCENTAGE OF THE MEAN FATIGUE STRENGTH OF A SOUND MARTENSITIC STEEL WELD.
FIG. 5.22 ILLUSTRATION OF WELD QUALITY ZONES FOR A GIVEN REDUCTION IN MEAN FATIGUE STRENGTH.
Mild Steel

Quenched & Tempered Steel

Undercut depth in inches.
N.O. denotes non-observable.
N.P. denotes not permitted.
1 in. = 25.4 mm.

FIG. 5.23 WELD QUALITIES FOR UNDERCUT.
LOP depth in. inches.  
N.O. denotes non-observable.  
N.P. denotes not permitted.  
1 in. = 25.4 mm.  

Mild Steel  

Quenched & Tempered Steel  

FIG. 5.24 WELD QUALITIES FOR LACK OF PENETRATION.
FIG. 5.25 WELD QUALITIES FOR LACK OF FUSION.

Lack of Fusion Depth

Mild Steel

Quenched & Tempered Steel

LOF Depth in inches.
N.O. denotes non-observable.
N.P. denotes not permitted.
1 in. = 25.4 mm.

Bridges & Buildings

A.W.S. Limits
Slag Inclusion length in inches.
N.O. denotes non-observable.
1 in. = 25.4 mm.

Mild Steel

- Buildings
  - 2/3 effective throat
  - 2/3 weld size
  - 3/4-inch

- Bridges
  - 1/3 effective throat
  - 1/3 weld size
  - 1/2-inch

Maximum Dimensions

A.W.S. Limits

FIG. 5.26 WELD QUALITIES FOR SLAG INCLUSIONS.
FIG. 5.27 WELD QUALITIES FOR POROSITY.
APPENDIX A

DETERMINATION OF RESIDUAL STRESSES IN WELDED HY-80 PLATE

A.1 Introduction

The magnitude of the residual stresses in four welded specimen types were measured using two full width and two narrow width specimens. The residual stresses of all specimens were determined by destructive relaxation techniques. Different techniques, however, were used for the full and narrow width specimens.

The specimens were fabricated from HY-80 steel and E11018 electrodes. The residual stress measurements were conducted only for the higher strength steel in the investigation because of the ready availability of such measurements for mild steels, and the lack thereof for higher strength steels.

A brief discussion of the residual welding stresses in welds is presented in Section A.2. The residual stress measurements for the narrow and full width specimens are respectively presented in Sections A.3 and A.4. In each of these sections the method for determining the residual stresses is outlined, the specimen configurations are described, and relevant observations of the measurements are discussed.

A.2 General Remarks - Residual Stresses
Residual stresses may be defined as those stresses which exist in a body in the absence of any externally applied loading (113). Several different types of residual stresses can exist in structures as a result of factors such as prior overloading, differential heating, welding, surface treatments, and lack of fit. The generic term "residual stress", however, will be used hereafter to indicate the self-balancing type of stress that exists in a weldment as a direct result of welding.

A number of studies have been directed towards measuring the distribution and magnitude of residual stresses. Typical distributions of the average residual stresses in a groove welded plate are shown in Fig. A.1. Longitudinal residual stresses act in a direction parallel to the axis of the weld as shown in Fig. A.1a. Large tensile stresses develop over a very narrow region near the weld as a result of the shrinkage of the weld metal as it cools. These tensile stresses are offset by compressive stresses of much lower magnitude over the remaining width of the plate. Transverse residual stresses act in a direction perpendicular to the axis of the weld as shown in Fig. A.1b. They are tensile in the middle portion of the weld length and compressive near the ends.

The average longitudinal residual stress in a weld can attain yield point magnitude. Measurements by Masubuchi et al. (114), Nordell (115), and Wilson et al. (116) have all shown longitudinal residual stresses of yield point magnitude in mild steel weldments. However, in investigations conducted by Masubuchi (114), and Odar et al. (117), the...
maximum residual stresses measured in high-strength steel weldments - 50,000 to 80,000 psi (345 to 552 MPa) - were not as high as the yield strength of the weld metal or the base metal.

Usually, the average transverse residual stress is considerably less than the yield strength of the base or weld metal. However, local values of the transverse residual stress through the thickness can at times reach yield point magnitude.

A.3 Narrow Width Specimens

Residual stresses were determined in two strip type specimens similar to those described in Section 3.2. The purpose of the measurements was to define the distribution and magnitude of residual stresses for use in evaluating their effect on the fatigue behavior.

The stresses in the specimens were determined using a uniaxial relaxation technique. The method consists of measuring the strain relaxed on the surface of a specimen as a result of successive sectioning. Strain gages are bonded to the surface of a specimen in the direction that residual stresses are to be determined. Blocks of material around the gages are then removed from the plate by saw cutting; the block must be cut as close to the gage as possible to unlock all the stress. Strain readings are recorded after each saw cut. The total relaxed strain is simply the difference between the initial and final strain readings.
The transverse residual stress, shown as $S_y$ in Fig. A.2a, was computed directly from the measured change in transverse surface strain resulting from successive sectioning. (To be consistent, the notation of the various directions of the residual stresses, whether longitudinal or transverse, will be as defined earlier with respect to the longitudinal axis of the weld.) The residual stresses in the longitudinal and thickness directions, shown respectively as $S_x$ and $S_z$ in Fig. A.2a, were assumed small enough to be neglected. The longitudinal stresses in the small specimens were almost entirely relieved because the specimens were cut and surface ground to a dimension of only 3/16-in. (4.8 mm) in the longitudinal direction. Some through thickness residual stresses, $S_z$, will undoubtedly exist in the central portion of the weld but, since the specimens were only 1-in. (25.4 mm) thick, they will be comparatively small. Indeed, measurements by Rosenthal et al. (118) on a 1-in. welded plate of mild steel confirm that the stresses in the thickness direction are small in comparison with longitudinal and transverse stresses.

The two specimens studied had different geometries and different loading histories. Specimen R1 was unnotched and had not been previously subjected to any externally applied load. Specimen R2-T5 contained a centrally located circular notch ($R/N=0.058$) and had been subjected to 3,416,750 cycles of loading; this specimen was previously Specimen T5 from the fatigue test series reported in Section 3.2.2.

The layout of the strain gages and saw cuts for Specimen R1 is shown
in Fig. A.2b. The gages were located symmetrically across the width of
the specimen and were spaced far enough apart to permit saw cutting.
The layout of the strain gages and saw cuts for Specimen R2-T5 is shown
in Fig. A.2c. Gages 1, 2, and 3 were attached before and monitored
during fatigue cycling; gages 4 and 5 were added afterwards for the
residual stress determinations.

The residual stresses calculated from the measured relaxed strains
are shown in Fig. A.3 for both specimens. The stress distribution for
Specimen R1, shown in Fig. A.3a, is nearly symmetrical with a zone of
high compressive stress in the middle third of the plate that is
balanced by a tensile stress zone on each side. The stress near the
surface and in the center was, no doubt, affected by factors such as
removal of the reinforcement by surface grinding, and the relative
position of the thin specimen removed from the original butt welded
plate.

The stress distribution for Specimen R2-T5, shown in Fig. A.3b, is
not symmetrical. A high compressive residual stress zone existed on one
side of the circular hole and was balanced by tensile residual stress
zones on the outside portions of the specimen. The lack of symmetry in
the distribution of the residual stresses is most likely the result of
factors such as the sequence of welding, a redistribution of the
stresses caused by not drilling the hole at exactly midwidth, a
relaxation of the stress on only one side of the hole during fatigue
cycling, or a combination of these factors. Regardless of the cause, it
is significant to note that the magnitude of the transverse residual stress is at no time greater than half the yield strength of the base metal.

A.4 Full Width Specimens

The residual stresses at selected locations were determined for two groove welded plates of different size. The purpose of these measurements was to determine the magnitude of the residual stresses for full width, higher strength steel weldments. The locations of the measurements were chosen in regions where the maximum residual stresses were expected to exist.

The residual stresses in the welded plates were determined using a relaxation technique developed by Rosenthal and Norton (118). The basic procedure consists of determining the change in strain in narrow rectangular blocks cut from a plate and then sliced into small sections. The measurements are decomposed into two parts: the strain relaxed due to removing a block from the plate, and the strain further relaxed due to splitting the block in two and then removing layers of material towards each surface.

The first operation is to measure the strain relaxed due to removing a block having full thickness of the plate and strain gages bonded back-to-back in the direction the residual stress is being measured, Fig. A.4a. The second operation is to measure the stress still locked
in the block. This is accomplished by measuring the relaxed strain on the surface as a result of splitting the block in two and shaving off material towards each surface.

Based on the assumption that the stress perpendicular to the long axis of the block is completely relieved as a result of cutting a narrow block from the plate, the value of stress relieved on the face of a longitudinal or transverse block is given by (118):

\[
S'_l = \frac{E}{1-\nu^2} \left[ \epsilon'_l + \nu \epsilon'_t \right] + \frac{E}{1-\nu^2} \left[ \nu \epsilon''_t + \frac{\nu^2 - \beta}{1-\beta} \epsilon''_l \right]
\]

\[
S'_t = \frac{E}{1-\nu^2} \left[ \epsilon'_t + \nu \epsilon'_l \right] + \frac{E}{1-\nu^2} \left[ \nu \epsilon''_l + \frac{\nu^2 - \beta}{1-\beta} \epsilon''_t \right]
\]  \tag{A.1}

where

\( S'_l, S'_t \) = value of stress relieved on the face of a longitudinal or transverse block as a result of removal from a plate,

\( \epsilon'_l, \epsilon'_t \) = value of strain relaxed on the face of a longitudinal or transverse block as a result of removal from a plate,

\( \epsilon''_l, \epsilon''_t \) = value of strain relaxed on the face of a longitudinal or transverse block as a result of splitting a block in two and removing layers of material,

\( E \) = elastic modulus of the material,

\( \nu \) = Poisson's ratio,

\( \beta \) = fraction of the stress which would not have been relaxed from a full size block - twice the thickness or longer - when using an undersize block.
Next, by cutting the block in two, both normal and shearing stresses are relieved at the interface. This stress relief is reflected on the top and bottom faces of the block as the stress redistributes. Because the linear law of relaxation is known to be valid, the value of stress relieved at any depth $\alpha$ can be readily computed from the amount of strain relaxed on the top and bottom faces of the block, Fig. A.4b. Considering the strain relaxed on the top and bottom faces to be the result of a tangential load and a couple acting at the interface, the equation for the stress $S''_\alpha$ relieved at any depth is given by (118):

$$S''_\alpha = (5\alpha - 4)S''_1 + (1 - \alpha)S''_2$$  \hspace{1cm} (A.2)

where $S''_1, S''_2$ = stress relieved on Surface 1 or 2 as a result of cutting a block in two,

$\alpha$ = fraction of the plate thickness between mid-thickness and Surface 1 as measured from Surface 2. (Note that $\alpha$ varies between 0.5 and 1.0.)

The stress, as a result of cutting the block in two, at any depth $\alpha$ between mid-thickness and Surface 2 as measured from Surface 1, may be obtained from Eq. A.2 by interchanging the subscripts 1 and 2.

The residual stress remaining in each half block can be further relaxed by progressively shaving off layers of material towards the
surface. The stress relieved at any depth $\alpha$ can be computed from the
stress relieved on each surface as (118):

$$S'''' = \frac{1 - \alpha}{2} \frac{dS_1}{da} - 2S_1 + 3 (1 - \alpha) \int_0^\alpha \frac{S_1 d\mu}{0.5} (1 - \mu)^2$$  \hspace{1cm} (A.3)

where $S'''' = \text{stress relieved at any depth } \alpha \text{ as a result of removing material from the interface,}$

$dS_1 = \text{change in stress on Surface } 1 \text{ as a result of}
\text{shaving off a layer of material } d\mu
\text{in thickness.}$

The total stress relieved at position $\alpha$, as a result of cutting the
block in two and removing layers of material from the center towards the
surface, is obtained by adding Eqs. A.2 and A.3.

$$S'' = E \left[ (5\alpha - 4)e''_1'^0 + (1 - \alpha)e''_2'^0 + \frac{1}{2} \frac{de''_1'}{da} (1 - \alpha) \right.$$

$$- 2 (e''_1 - e''_1'^0) + 3 (1 - \alpha) \int_0^\alpha \frac{(e''_1 - e''_1'^0) d\mu}{(1 - \mu)^2} \right]$$  \hspace{1cm} (A.4)

(Note that this equation is for a value of $\alpha$ measured from Surface 2; an
equation similar to Eq. A.4 can be written for a value of $\alpha$ measured
from Surface 1 by interchanging the subscripts.)

The value of stress $S''$ from Eq. A.4 is added to the stress $S'''$',
determined by linearly interpolating between the values given by
Eq. A.1, to obtain the total relaxed stress at any depth \( a \) beneath the surface.

Different size specimens of 1-in. (25.4 mm) thickness were selected to investigate weld length effects. The dimensions of the specimens and locations of the residual stress blocks are shown in Fig. A.5. The large specimen, Plate 4A in Fig. A.5a, was fabricated to produce four of the 5-in. (127 mm) wide specimens described in Section 3.3. The orientation and number of residual stress blocks was dictated by the layout of the 5-in. wide specimens; this restriction permitted only one stress block to be used in the middle portion of the plate, and a less than full size longitudinal block to be used at the end. The smaller specimens, Plates RS-A and RS-B in Fig. A.5b, were selected to be 5-in. wide to match the width of the full width specimens described in Section 3.3. Due to their short weld length, however, two plates had to be prepared to accommodate all the residual stress blocks required for measurements in the middle and at the ends. An undersize block was used at the end of Plate RS-A so that the center of the outside block was as close to the edge as possible.

The welding procedure for Plates 4A, RS-A, and RS-B was essentially the same as that used to fabricate the specimens described in Section 3.3; the welding procedure for Plates RS-A and RS-B only differed from the previously described welding procedure in that no intentional weld discontinuities were deposited.
Strain gages were bonded to the top and bottom surface of the specimens where residual stress blocks were to be located. Uni-directional gages were used in all locations except in the middle block of Plate 4A where a two-element strain gage was used. All gages had a 1/8-in. (3.2 mm) gage length. A photograph of Plate 4A with the strain gages and measuring equipment is shown in Fig. A.6.

The residual stress blocks were removed from the plates, cut in two, and then shaved from the center towards the surface in 0.050-in. (1.3 mm) layers. During the material saw cutting, the temperature was never allowed to exceed 150°F (65.6°C), and during the shaving operations the temperature never increased above 100°F (37.8°C). The surfaces of the blocks were allowed to cool to room temperature before strain readings were taken.

The computed values of the residual stresses as a result of removing Block BT 1 from Plate RS-B, based on the relaxation method, are presented in Table A.1. A plot of the transverse strains relaxed on the surface as a result of cutting the block in two and shaving off successive layers of material is shown in Fig. A.7. The value of strain relaxed on each face was obtained by extrapolating the data given in Fig. A.7 to zero thickness. The values of the stresses relaxed at various depths through the thickness as a result of cutting and shaving were computed by means of Eq. A.4, and are summarized in Table A.2.
The total relaxed stress at a depth $a$ below the surface, $S_a$, was obtained by adding the stress relaxed by cutting and shaving ($S''_a$ of Table A.2) to the stress relaxed by removing the block from the plate ($S'_a$ based on linear interpolation of $S'_t$ at $a$ between Surfaces 1 and 2 in Table A.1).

The procedure for calculating the residual stresses, as outlined above, was performed for blocks located in both the small and large plates, Fig. A.5. The results in terms of residual stresses through the thickness are presented in Figs. A.8 and A.9 for the small plates and in Figs. A.10 and A.11 for the large plate.

Several observations can be made, based on the limited measurements made on the full width specimens.

First, the distribution of stress through the thickness for both the small and large plates varies significantly and may be unsymmetrical. Unsymmetrical residual stress distributions have been observed in high strength steels by other investigators (117,119,120) and are most likely the result of a combination of factors such as uneven placement of multipass weld layers, gouging and redeposition of weld metal to introduce discontinuities, and inaccuracies or simplifications in the analysis.

Second, the residual stress values near the weld surfaces are very low. This is contrary to the high tensile residual stresses that
normally exist near the surfaces of as-welded plates. A low surface stress is logical, however, because the top weld beads were ground off flush with the plate surface, thereby relieving most if not all of the surface residual stresses.

Third, the magnitude of the residual stresses in the wide plate is greater than in the narrow plate. The build-up of residual stress with weld length is well known and was not unexpected.

Fourth, the maximum longitudinal stress through the thickness is observed to be locally as high as the yield strength of the weld metal, but the average stress through the thickness is only about 71,900 psi (496 MPa). This value is larger than the longitudinal residual stress measurements reported by Masubuchi and Martin (114) of 50,000 psi (345 MPa) in a SAE 4340 steel.

Finally, the transverse residual stresses, for all welds examined, were compressive near mid-thickness. This is a significant factor when considering the effect of internal weld discontinuities on the overall fatigue behavior.
### TABLE A.1
COMPUTED STRESS RELAXED BY REMOVING BLOCK BT-1 FROM PLATE RS-B

<table>
<thead>
<tr>
<th>Surface</th>
<th>Longitudinal Strain $\text{in/in} \times 10^{-6}$</th>
<th>Transverse Strain $\text{in/in} \times 10^{-6}$</th>
<th>Longitudinal Stress ksi</th>
<th>Transverse Stress ksi</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$e_l'(1)$</td>
<td>$e_l''(2)$</td>
<td>$e_t'(1)$</td>
<td>$e_t''(2)$</td>
</tr>
<tr>
<td>1</td>
<td>1830</td>
<td>-1740</td>
<td>-1020</td>
<td>335</td>
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<tr>
<td>2</td>
<td>1510</td>
<td>-1585</td>
<td>-695</td>
<td>890</td>
</tr>
</tbody>
</table>

(1) Value of strain relaxed by removing block from plate.

(2) Data from Fig. A.7.

(3) Computed using Eq. A.1.

$1 \text{ ksi} = 6.895 \text{ MPa}$. 
### TABLE A.2

**COMPUTED RELAXED STRESS FROM CUTTING AND SHAVING**

**BLOCK BT-1**

<table>
<thead>
<tr>
<th>( \alpha )</th>
<th>( e'' )</th>
<th>( \Delta e'' / \Delta x )</th>
<th>( F = \int_0^x \frac{e'' - e''^o}{0.5(1-\alpha)^2} , \Delta x )</th>
<th>( \frac{1}{2} (1-\alpha) \frac{\Delta e''}{\Delta x} )</th>
<th>( 3(1-\alpha)F )</th>
<th>Sum(1)to(3)</th>
<th>( S'' = E\Delta e )</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.500</td>
<td>1085</td>
<td>-2200</td>
<td>0</td>
<td>-1145</td>
<td>-565</td>
<td>0</td>
<td>-1710</td>
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<tr>
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<td>820</td>
<td>-6400</td>
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<td>-945</td>
<td>-965</td>
<td>-40</td>
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<td>-360</td>
<td>-540</td>
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<td>-170</td>
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<td>-720</td>
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<td></td>
<td></td>
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<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

(Surface 1 \( e''^o = 1085 \))
### TABLE A.2 (continued)

**COMPUTED RELAXED STRESS FROM CUTTING AND SHAVING**

**BLOCK BT-1**

<table>
<thead>
<tr>
<th>( \alpha )</th>
<th>( \varepsilon'' )</th>
<th>( \frac{d\varepsilon''}{d\alpha} )</th>
<th>( F = \int_{0.5}^{\alpha} \frac{(5\alpha-4)}{(1-\alpha)^2} \frac{d\varepsilon''}{d\alpha} )</th>
<th>( \Delta \varepsilon )</th>
<th>( \Delta \varepsilon^{(1)} )</th>
<th>( \Delta \varepsilon^{(2)} )</th>
<th>( \Delta \varepsilon^{(3)} )</th>
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<td>-740</td>
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<td>-315</td>
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<td>-</td>
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<td>-630</td>
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</table>

* From Fig. A.7
FIG. A.1 TYPICAL DISTRIBUTION OF RESIDUAL STRESSES IN A BUTT WELD.

(a) Longitudinal Residual Stress, $S_x$, Along YY

(b) Transverse Residual Stress, $S_y$, Along XX
(a) Distribution of Residual Stresses in Strip Specimen

(b) Location of Strain Gages for Specimen R1

(c) Location of Strain Gages for Specimen R2-T5

Note: Gages 1, 2, 3 attached to specimen for duration of fatigue life. Gages 4 and 5 attached for residual stress measurement.

FIG. A.2 NOTATION AND SECTIONING DETAILS FOR SMALL SCALE RESIDUAL STRESS SPECIMENS.
FIG. A.3 RELAXED STRESS OF SMALL SCALE SPECIMENS.
(a) Removal of Residual Stress Block with Strain Gage Attached

(b) Stress Relieved at Level $\alpha$ as a Result of Sawing Block in Two

FIG. A.4 REMOVAL AND SPLITTING OF STRESS BLOCKS. AFTER ROENTHAL AND NORTON (118).
(a) Plate 4A and Layout of Blocks

(b) Plates RS-A and RS-B and Layout of Blocks

FIG. A.5 LARGE SCALE SPECIMEN DIMENSIONS AND
RESIDUAL STRESS BLOCK LAYOUTS.
FIG. A.6 STRAIN GAGES AND MEASURING EQUIPMENT FOR PLATE 4A.
FIG. A.7  STRAIN RELAXED ON SURFACES OF TRANSVERSE BLOCK BT-1 REMOVED FROM 1-IN. (25.4 MM) THICK PLATE.
FIG. A.8 DISTRIBUTION OF RESIDUAL STRESS THROUGH THICKNESS OF WELD-BLOCKS AT CENTER OF PLATES RS-A AND RS-B.
FIG. A.9 DISTRIBUTION OF RESIDUAL STRESS THROUGH THICKNESS OF WELD-BLOCKS NEAR EDGE OF PLATES RS-A AND RS-B.
FIG. A.10 DISTRIBUTION OF RESIDUAL STRESS THROUGH THICKNESS OF WELD-BLOCK NEAR THE MIDDLE OF PLATE 4A.
FIG. A.11 DISTRIBUTION OF RESIDUAL STRESS THROUGH THICKNESS OF WELD-BLOCKS NEAR THE EDGES OF PLATE 4A.
B.1. Introduction

The stress-intensity factor, $K$, is a single term parameter that describes the state of stress in the vicinity of a crack tip. The value of the stress-intensity factor is a function of the crack size, the crack position, and the remotely applied stress. The units of $K$ are expressed as ksi $(\text{in})^{0.5}(\text{N(mm)})^{-1.5}$.

The stress-intensity factor does not by itself have a direct physical significance as do concepts such as force, length, area, and stress. However, for a specific material, the stress-intensity factor does have a limiting or critical value. The critical stress-intensity factor, $K_c$, represents the static load at which unstable, rapid crack growth occurs. The value of $K_c$ for a particular temperature depends on the specimen thickness. The limiting value of $K_c$ for conditions of maximum (plane strain) restraint is generally denoted as $K_{IC}$; a similar definition exists for the limiting value of the stress-intensity factor for high strain rate dynamic loadings, $K_{ID}$. The material limitations of the stress-intensity factor, determined by standardized plane strain test methods using either the notch-bend specimen of compact-tension specimen, offer a quantitative value for comparison with the calculated value of the stress-intensity factor, $K_T$, for a given set of loading and crack size criteria.
The method of analysis in elastic solids depends on the particular type(s) of relative crack surface displacement. The basic modes of deformation, shown in Fig. B.1a, can be combined to describe most common types of crack tip displacements: in Mode I, the opening mode, the crack surfaces directly separate; in Mode II, the edge sliding mode, the crack surfaces slide normal to the crack front and remain in the crack plane; and in Mode III, the shear mode, the crack surfaces slide parallel to the crack front and remain in the plane of the crack.

Irwin (121) developed expressions for the near crack tip stresses and displacements for the three basic deformation modes using a stress function developed by Westergaard (122). In these expressions, a single term parameter $K_I, K_{II}, K_{III}$ is used to describe the magnitude of the stress state corresponding to Mode I, II, and III, respectively. Of the three deformation modes, the opening mode stress-intensity factor range, $\Delta K_I$, is most commonly used in fracture mechanics to describe the macroscopic subcritical crack growth. The expressions developed by Irwin (121) for Mode I near crack tip elastic stresses and displacements are given as follows:

\[
\begin{align*}
\sigma_x &= K_I (2\pi r)^{-0.5} \cos \frac{1}{2} \theta \left[1 - \sin \frac{1}{2} \theta \sin \frac{3}{2} \theta\right] \\
\sigma_y &= K_I (2\pi r)^{-0.5} \cos \frac{1}{2} \theta \left[1 + \sin \frac{1}{2} \theta \sin \frac{3}{2} \theta\right] \\
\tau_{xy} &= K_I (2\pi r)^{-0.5} \sin \frac{1}{2} \theta \cos \frac{1}{2} \theta \cos \frac{3}{2} \theta \\
u &= \frac{K_I}{G} \left(\frac{r}{2\pi}\right)^{0.5} \cos \frac{1}{2} \theta \left[1 - 2v + \sin \frac{1}{2} \theta\right]
\end{align*}
\]
\begin{align*}
v &= \frac{K}{G} \left( \frac{r}{2\pi} \right)^{0.5} \sin \frac{1}{2} \theta \left[ 2 - 2\nu - \cos^2 \frac{\theta}{2} \right] \\
w &= 0
\end{align*}

where \( r, \theta, \sigma_x, \sigma_y, \tau_{xy} \) = the coordinates and stress components shown in Fig. B.1b,

\( u, v, w \) = displacements in the \( x, y, \) and \( z \) directions, respectively,

\( G \) = shear modulus,

\( \nu \) = Poisson's ratio.

In Eq. B.1 the stresses apply for either plane stress or plane strain conditions, while the displacements apply only for conditions of plane strain. A state of plane stress can be obtained by setting \( \sigma_z = 0 \) and replacing Poisson's ratio, \( \nu \), in the displacements with an appropriate value (111).

The stresses and strains in the crack tip region described by Eqs. B.1 are exact only in the case of zero plastic strain, which occurs in completely brittle materials such as glass. However, sufficient accuracy is obtained if the plastic zone ahead of the crack tip is small in comparison with the region around the crack, in which case the stress-intensity factor yields a satisfactory approximation of the exact elastic-stress field (58). A small amount of yielding ahead of the crack tip does not significantly alter the stress distribution away from the crack front, Fig. B.2. A large plastic zone, however, will affect the accuracy attained by use of the linear-elastic stress field described by Eqs. B.1.
Paris et al. (44) have shown that the fluctuation in stress-intensity factor, $\Delta K$, is directly related to the subcritical fatigue crack growth rate. This is valuable information in that it allows a numerical estimate of the fatigue crack propagation life of a cracked component or member to be made; the analysis depends primarily upon the material constants describing the rate of crack growth and the corresponding range in stress-intensity factor. Secondary effects that can affect the crack propagation behavior include mean stress, frequency of loading, and cyclic wave forms. Numerous experimental programs have been conducted to establish fatigue crack growth rates for a wide range of material types and strengths. The results of these investigations are readily available in the literature. Therefore, with knowledge of appropriate crack growth relationships and material constants, a crack propagation analysis depends on the determination of the stress-intensity factors for each particular application.

Section B.2 outlines several techniques available to determine stress-intensity factors, and Section B.3 demonstrates the development of an approximate method for determining the stress-intensity factors used to evaluate a part of the experimental results presented in Chapter 4.

B.2 General Methods For Calculating Stress-Intensity Factors

Relationships for the determination of stress-intensity factors for common crack configurations are readily available (111,123). However,
standard solutions can not be used in modeling structures containing cracks located at notches, fillets, or unusual locations that are not directly amenable to handbook solutions. These situations require solution by a more direct method.

Various classical techniques have been used to determine stress-intensity factors; several of which are summarized below. The Z stress-function developed by Westergaard (122), the complex stress function by Muskhelishvili (124), and integral equation procedures by Bueckner (125) have all been used to solve a number of crack problems. Various strip problems have been solved by Isida (126,127) using mapping functions, and by Kobayashi et al. (128) and Newman (129) using collocation procedures. Bowie (130,131) has used polynomial mapping functions to solve the cases of single and double cracks emanating from a hole in an infinite plate, and double edge cracks in a finite width plate. Sneddon (132) has solved the problem of a circular disk crack in an infinite solid using fourier transforms.

Use of the finite element method is a relatively new approach for calculating stress-intensity factors. This method is particularly attractive for geometries too complicated to be treated by classical methods. A typical approach taken by several analysts (133,134,135, 136) is to develop a "cracked" hybrid element that surrounds the crack tip. The near crack tip stresses and displacements are used as boundary conditions for the nodes of the hybrid element, while the remaining mesh away from the crack tip is composed of typical finite elements. In a
somewhat different approach, Leverenz (137) has determined the stress-intensity factor by using the finite element method to evaluate the J-integral around a crack tip, and then utilized a relationship between the J-integral and $K$.

An alternate approach to the above methods has been described by Albrecht et al. (138) and Zettlemoyer et al. (139), and consists of using correction factors to modify accepted solutions for two-dimensional and three-dimensional crack problems. The advantage of this procedure is that it provides an accurate estimate of the stress-intensity factor for difficult structural geometries at only a fraction of the manhours or computational effort associated with closed form numerical solutions or solutions obtained using special crack tip finite elements. Albrecht's approach has been used in this investigation for calculating stress-intensity factors because of the reasonable accuracy and computational ease of the method.

Simply stated, the solution by Albrecht et al. (138) for the stress-intensity factor for any general structural configuration is given as follows:

$$K = F(a) S \sqrt{\pi a} \tag{B.2}$$

where

$$F(a) = F_E F_S F_W F_G,$$

$F_E, F_S, F_W, F_G =$ crack size dependent correction factors that account for the effects of an elliptical crack front, free surface, finite plate width, and nonuniform opening stresses, respectively,

$S =$ remotely applied uniform stress,
\[ a = \text{half crack width in an infinite plate.} \]

The correction factors, \( F(a) \), act to transform the solution of the stress-intensity factor for an infinite plate containing a crack of width \( 2a \) into the desired \( K \)-solution for a particular situation defined by individual correction factors.

The value of \( F_E \) for an infinite solid containing an elliptical crack front is obtained by determining the constant that amplifies the solution for a tunnel crack in an infinite plate subjected to remotely applied uniform tension. Irwin (140) has shown that the stress-intensity factor depends on a complete elliptical integral of the second kind, \( E_K \), and that the maximum value occurs at the intersection of the minor axis of the ellipse and the crack front.

\[
K = \frac{1}{E_K} S(\pi a)^{0.5} \tag{B.3}
\]

where \( E_K = \int_0^{\pi/2} \left[ 1 - \left(1 - \frac{a^2}{d_e^2}\right) \sin^2 \theta \right]^{0.5} d\theta \]

\[ \frac{a}{d_e} = \text{ratio of the minor to major axes} \]

To be consistent with the definition of the correction factors, the value of \( F_E \) is the constant that amplifies the \( K_I \) factor for a crack in an infinite plate.
$$F_E = \frac{1}{E_k}$$  \hspace{1cm} \text{(B.4)}

The value of $F_E$ accounts for an elliptical crack front of relative size $a/d_e$ in a three-dimensional solid. For two-dimensional crack problems $F_E$ has no real meaning and is assigned a value of 1.0 - i.e., no effect on $K$.

A free surface correction factor, $F_S$, of 1.12 accounts for an edge crack of depth $a$ in a semi-infinite plate subjected to a uniformly applied remote stress. For a nonuniformly applied remote stress, the value of $F_S$ can be calculated using superposition techniques with an expression developed by Hartranft and Sih (141) for a semi-infinite plate with the crack faces loaded by a pair of splitting forces. (Superposition or Green's function techniques can be used to calculate the stress-intensity factor in a plate in which the stress distribution is known in a similar, but uncracked, plate. The $K$-solution for a plate with only the crack surfaces loaded by the prescribed stress distribution is added to the $K$-solution for a remotely loaded plate with the crack surfaces held shut by an identical stress distribution applied in the opposite direction.)

The correction factor for finite plate width, $F_W$, accounts for the absence of normal and shear stresses along the plate edge for a particular relative ratio of crack to plate width. Fedderson (142) discovered a remarkably accurate and simple correction:
\[ F_{W} = \left( \sec \frac{\pi a}{W} \right)^{0.5} \] 

where \( a \) is the half crack width, and \( W \) is the finite plate width. Eq. B.5 is within one percent of a fourteenth order polynomial expression for most \( a/W \) values, and within two percent for an \( a/W \) ratio of 0.45.

A geometrical discontinuity can elevate the stress in the region of a crack. (This increased stress should not be confused with the stress field due to the crack tip.) Albrecht et al. (138) have shown, using superposition techniques, that the correction factor for geometry is given by

\[ F_{G} = \frac{2}{\pi} \sum_{i=1}^{n} \frac{S_i}{S} \left[ \arcsin \frac{h_{i+1}}{a} - \arcsin \frac{h_i}{a} \right] \]

where

- \( n \) = number of intervals over which the stresses are being summed,
- \( h_i, h_{i+1} \) = distance from midwidth of the crack to the beginning and end of the increment, respectively, for the \( i \)-th interval,
- \( S_i/S \) = ratio for the mean stress for the \( i \)-th interval to the remotely applied stress.

Once the stress distribution for an uncracked detail has been determined, using either a closed form solution or finite element analysis, the geometric correction factor for the stress elevation is determined using Eq. B.6. Alternatively, the value of \( F_{G} \) can be determined by direct integration if a simple closed form solution for the stress distribution of the uncracked detail exists.
Determination of the stress-intensity factor for a plate with a circular hole is demonstrated in this section using the approximate method given by Albrecht et al. (138). The $K$-solution for a circular hole in a finite width plate was selected because of its applicability for analysis of the fatigue data in Chapter 4. Each of the various components of $F(a)$ are considered separately before combining them together for a complete solution.

A value of $F_E = 1.0$ has been assumed for cracks through the entire thickness of the specimen emanating from the hole in a state of plane stress. (For small elliptical cracks forming at mid-thickness of the notch, the value of the elliptical crack front correction factor would be dictated by the appropriate $a/d_e$ ratio in the elliptical integral of Eq. B.4.)

For small ratios of the crack width to the hole radius, $a/R_N$, the crack responds as it would near a free surface. The effect of the hole on the stress field diminishes as the crack propagates away from the hole edge. Albrecht et al. (138) suggest using a free surface correction factor that diminishes for increasing ratios of the crack width to hole radius.

$$F_s = 1 + 0.12 \left(1 + \frac{a}{R_N}\right)^{-2} \quad \text{(B.7)}$$
The value of \( F_s \) diminishes to only 1.03 for a crack width equal to the hole radius, and to 1.01 for a crack width of two and one half times the hole radius.

The location and size of cracks that emanate from a hole with respect to the plate width are variables that influence the correction factor for finite width plates, \( F_W \). Isida (143) has examined the stress-intensity factor of eccentric cracks in finite width plates using a Laurent series expansion. His solution indicates that eccentric cracks act in a fashion similar to centrally cracked plates of a reduced width. The following simplified expression, somewhat similar to the secant correction factor, was suggested (143):

\[
F_W = \left[ \sec \frac{\pi \lambda}{2} \frac{\sin 2\lambda \delta}{2\lambda \delta} \right]^{0.5} \quad \text{for } \lambda \leq 0.75 \tag{B.8}
\]

where \( \lambda = \text{ratio of the half crack width to the distance from the middle of the crack to the closest plate edge} \), \( \delta = \text{ratio of the offset of the center of the crack from midwidth of the plate to the half plate width} \).

The error in using Eq. B.8 is less than one percent within the range indicated (58).

Kobayashi (144) suggested a method for computing the stress-intensity factor for cracks emanating from a hole in an infinite plate. The method utilizes superposition techniques to remove tractions
on the crack surfaces in the neighborhood of a hole. The distribution of the surface tractions are determined from the elasticity solution for a hole in an infinite plate (145). The geometric correction factor is computed using a pair of splitting forces to remove the surface tractions over the entire crack length (144).

\[ F_G = \left(1 + \frac{R_N}{a}\right)^{0.5} \left[1 - \frac{2}{\pi} \arcsin \left(\frac{R_N}{R_N + a}\right) - \left(\frac{R_N}{R_N + a}\right)^2 \right] \left[1 + \left(\frac{R_N}{R_N + a}\right)^2 \right] \left[1 - \left(\frac{R_N}{R_N + a}\right)^2 \right]^{0.5} \]

(B.9)

However, for small crack widths compared to the hole radius, the correction factor for the stress-intensity factor solution of Eq. B.9 is less than the solution reported by Bowie (130) using a polynomial mapping function. To rectify this discrepancy, an empirical correction factor, \( f^* \), for Eq. B.9 was found so that the solution for an infinite plate closely conformed to Bowie's solution.

\[ K_I = F_S F_G f^* S(\pi a)^{0.5} \]

(B.10)

where

\[ f^* = 1.12 - 0.15 \left(\frac{a}{R_N}\right). \]

The difference between the values for Eq. B.10 and Bowie's solution are less than 2 1/4 percent for a crack size less than the hole radius.

Once the cracks have propagated to a width greater than the size of the hole radius, the effect of the hole diminishes considerably. Bowie (130) states that, "For large crack lengths, say \( L > 1 \), ... the effect of the stress field caused by the circular hole is negligible insofar as
the critical stress is concerned." (L is the sum of the hole radius and the crack width.) For computational purposes, both Bowie (130) and Broek et al. (146) suggest using an "effective crack length", a crack width composed of the hole diameter plus the actual crack width, for cases when the effect of the stress field caused by the hole becomes small.

The complete solution of the stress-intensity factor for a finite width plate containing an eccentric hole can be written by combining the correction factors as described above.

For $0 \leq L/W \leq 2 \left( \frac{R_N}{W} \right)$ and $\frac{L}{W} \leq 0.75 (0.5 - \psi)$

$$K_I = S\sqrt{\pi L} \left[ 1 - \frac{2}{\pi} \left( \arcsin \left( \frac{R_N}{L} \right) - \frac{R_N}{L} \left[ 1 + \frac{R_N}{L} \right]^2 \right) \right] .$$

$$\left[ 1 - \left( \frac{R_N}{L} \right)^2 \right]^{0.5} \left[ 1.12 - 0.15 \left( \frac{a}{R_N} \right)^{0.5} \right] \left[ 1 + 0.12 \left( \frac{L}{R_N} \right)^{-2} \right] .$$

$$\left\{ \sec \left[ \frac{\pi (L/W)}{2(0.5 - \psi)} \right] \right\} .$$

$$\sin \left[ \frac{4(L/W)\psi}{(0.5 - \psi)} \right]^{0.5} \right\} .$$

\[
\text{(B.11)}
\]
when \( L/W \geq 2 \left( \frac{R_N}{W} \right) \) and \( \frac{L}{W} < 0.75 \ (0.5 - \psi) \)

\[
K_I = S \sqrt{\pi L} \cdot \left[ 1 + 0.12 \left( \frac{L}{R_N} \right)^{-2} \right] \left\{ \sec \left[ \frac{\pi (L/W)}{2(0.5 - \psi)} \right] \right. \\
\left. \sin \frac{4(L/W)\psi}{(0.5-\psi)} \right\} \\
\left. \frac{4(L/W)\psi}{(0.5-\psi)} \right\}
\]  

(B.12)

where

- \( R_N = \) hole radius,
- \( L = a + R_N \),
- \( W = \) finite plate width,
- \( \psi = \) ratio of the offset of the hole from midwidth of the plate to the full plate width.

Although use of Eqs. B.11 and B.12 are tedious for hand calculations, they are easily adaptable for use in a computer.
(a) The Three Basic Modes of Crack Surface Displacements.

(b) Coordinates Measured from Leading Edge of a Crack and Stress Components in the Crack Tip Stress Field.

FIG. B.1  BASIC MODES OF CRACK SURFACE DISPLACEMENTS AND COORDINATES MEASURED FROM LEADING EDGE OF A CRACK. AFTER PARIS AND SIH (111).
FIG. B.2 EFFECT OF SMALL PLASTIC ZONE ON NOMINAL STRESS FIELD. AFTER ROLFE AND BARSOM (58).