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FATIGUE CRACK GROWTH and COALESCENCE in a NOTCH REGION

by

Peter Richard Frise M.Sc.(Eng)

**A thesis submitted to the Faculty of Graduate Studies
and Research in partial fulfillment of the
requirements for the Degree of Doctor of Philosophy.**

**DEPARTMENT of MECHANICAL and AEROSPACE ENGINEERING
CARLETON UNIVERSITY**

OTTAWA, CANADA

September, 1990

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of over two thousand welded Liberty cargo ships suffered serious structural failures shortly after being built. There have been many more cases of fracture over the years as new high strength materials have come into use. The Liberty ship mishaps and other failures of ships and barges caused Lloyds of London and the American Bureau of Shipping to begin to regulate the design and steel composition of welded ships in the late 1940s. This sparked interest in the phenomenon of the fracture of materials. Since then, several major bridge collapses and problems with various aircraft programs have expanded the field to include other materials such as titanium and aluminum.

1.2 The Canadian Offshore Research Effort

The current research activity in Canada, and in other energy producing countries, into the fracture mechanics of welded steel structures is directed towards improving the reliability of the properties of weldments and finding new ways of predicting the life of welded steel structures. Clearly, if the fatigue life of a structure can be predicted more accurately and reliably, the need for costly and dangerous inspections will be decreased. Repairs and inspections can be carried out on a scheduled basis rather than in an emergency and the chances of a disastrous accident will be decreased.

Because of the inherent limitations of the S-N method, builders and regulatory authorities have begun to investigate a more realistic approach to fatigue design which does account for pre-existing flaws in the structure. This approach is known as Linear Elastic Fracture Mechanics or LEFM. The early leaders in the field of offshore fracture mechanics were the European Economic Community members. The British, Norwegians,

The undersigned hereby recommend to the Faculty of Graduate Studies and Research acceptance of the thesis, "Fatigue Crack Growth and Coalescence in a Notch Region" by Peter Richard Frise, B.Sc.(Eng), M.Sc.(Eng) in partial fulfilment of the requirements for the Degree of Doctor of Philosophy.

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ABSTRACT

An experimental and analytical study of the process of fatigue crack growth and coalescence in the region of a notch has been performed. The experimental program consisted of constant amplitude fatigue tests of 24 steel bar specimens. Each bar had a 135° vee notch machined across the middle.

An important part of the experimental program was the monitoring of the fatigue cracks by the Alternating Current Potential Drop (ACPD) method. To better understand this technique, a preliminary part of the test program was a study of the electric field induced in the specimen by the ACPD equipment. This study showed that the field is not uniform. This has led to the development of a data collection probe array that compensates for this lack of uniformity and yields crack growth measurements that are accurate even before being calibrated with visible beachmarks after the completion of the test.

The results of the experimental program are examined and compared to existing design codes that prescribe a method for dealing with adjacent cracks. It has been found in the present study that pre-coalescence interaction between adjacent cracks is not evident and, in fact, that coalescence itself is not an instantaneous process as the design codes suggest. Accordingly, a new crack shape development model has been developed that describes the coalescence process and accounts for the fact that the specimen has a finite width and the crack will eventually break through the edges and grow as a straight fronted crack. It has been concluded that the codes are over-conservative and are not realistic in the manner in which they treat the problem of adjacent fatigue cracks.

The experimental results were compared to computer predictions of fatigue crack growth using software that performs an integration of the Paris crack growth law and incorporates the new crack shape development and crack coalescence model. The predictions were found to agree reasonably well with the lives actually achieved by the test specimens.

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To Catherine and Sheila

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LIST OF SYMBOLS

S	Stress Range
N	Stress Cycles
x	Spatial Coordinate
a	Crack Depth
c	Crack Half-Surface Length
$\sigma_x, \sigma_y, \sigma_z$	X, Y, and Z Direction Stresses
$\tau_{xy}, \tau_{xz}, \tau_{yz}$	Shear Stress Components
μ	Poisson's Ratio
r, θ	Polar Coordinates at the Crack Tip
K_I, K_{II}, K_{III}	Modes I, II and III Stress Intensity Factors
K_{IC}	Mode I Critical Stress Intensity Factor
Y	Geometry Coefficient for Stress Intensity Factor
σ_{nom}	Nominal Stress
$m(x,a)$	Weight Function
σ_R	Reference Stress
K_R	Reference Stress Intensity Factor
E	Young's Modulus
H	Young's Modulus for Plane Stress or $E/(1 - \mu^2)$ for Plane Strain
$u(x,a)$	Crack Opening Displacement Field

P	Crack Opening Force
b	Separation Distance for the Two Forces - P
da/dN	Crack Growth Rate in the Depth Direction
dc/dN	Crack Growth Rate at the Surface Breaking Point
ΔK	Stress Intensity Factor Range (assumed to be mode I)
C, m	Crack Growth Law Constants
R	Stress Ratio - $(\sigma_{min}/\sigma_{max})$
ΔK_{th}	Threshold Stress Intensity Factor Range
K_{max}	Maximum Stress Intensity Factor
k	Crack Shape Development Coefficient
n_i	Number of Fatigue Cycles at Stress Level i
N_i	Fatigue Life at Stress Level i
D	Average Grain Size
ρ	Crack Tip Radius
r_o	Crack Tip Plastic Zone Size
τ	Thickness of the Conducting Skin of an AC Field
μ_r, μ_o	Relative Permeability and Permeability of Free Space
σ	Electrical Conductivity
f	Electrical Field Frequency
δ	Potential Drop Probe Spacing
V	Potential Drop of a Crack Spanning Channel
V_{REF}	Potential Drop of a Reference Probe Channel

W	Width of the Specimen
L	Length of the Specimen
T	Thickness of the Specimen
k_I, b_I, k_{II}, b_{II}	Crack Shape Development Coefficients for Double Crack Specimens
a_i	Depth of Initial Starter Cracks
c_i	Half Width of Each Initial Starter Crack
a_{coal}	Crack Depth at Which Coalescence Occurs
c_{coal}	Half-Width of Coalesced Crack
SS	Centre to Centre Spacing of Initial Starter Cracks
a_t	Crack Depth at Which Edge Break-Through Occurs
N_i	Number of Cycles to Crack Initiation of the Specimen
N_f	Total Life of the Specimen to Failure

Chapter 1 Introduction

1.0 General

The world's search for new reserves of petroleum has sparked a large international research effort into ways of ensuring the integrity of the large steel structures that are used in the exploration and production of oil and gas. There are several types of offshore structures (Figure 1.1), but they all have one thing in common: they are subjected to time-varying-loads. This means that fatigue is a major consideration in the design, fabrication and operation of these structures.

The offshore oil platform construction industry still employs the traditional S-N approach to fatigue design that has been used for well over 150 years. The difficulty with this approach is that it assumes that no flaws exist in the structure at the outset of its service life. Experience with welded structures over the years has demonstrated that this assumption is not valid. It has been found that virtually all large welded structures have

flaws of significant size before they even leave the builder's yard, Almar-Naess et al {1}.

From an operational standpoint, the Canadian drilling environment is much more demanding than anywhere else in the world. The ambient temperature and wave loading are every bit as severe as in the North Sea off the United Kingdom. Also, the violent storms and icing conditions in the Arctic and North Atlantic can drastically increase the top hamper of a structure, causing problems with the stability and ballast control of floating platforms and overloading of all types of structures. This is considered to have been the cause of the Ocean Ranger disaster in the early 1980's. The effects of iceberg collisions and imbedding of platforms in shifting pack ice also place unprecedented loads on offshore structures.

One of the major operating difficulties, and a source of great expense in the North Sea oil fields, has been the underwater inspection of offshore platforms. A below-decks inspection of a drilling platform can cost millions of dollars even for a check of only certain key nodes. Repairing the inevitable damage is even more expensive and dangerous as well as costing the operator expensive down-time. Inspection and repair operations on an ice-bound Arctic oil rig would be even more difficult, expensive, and dangerous than equivalent procedures in the North Sea.

The structural integrity of offshore platforms is of great concern for several reasons. One of the major fears is that of damage to the environment in the event of a platform collapse or storage tank leak. The fragile Arctic and East coast ecosystems would take generations to recover from a major spill such as that from the recent Exxon Valdez tanker grounding. The safety of the drilling crews is also a problem that must be

addressed. The extreme remoteness of the oil fields and the very poor weather conditions that prevail for much of the year makes the quick evacuation of the rig in the event of an accident uncertain. The key to operating safely and efficiently in the Arctic and North Atlantic oceans is to design the structures to be both stronger when new and more reliable in service.

1.1 Fracture Mechanics - An Historical Perspective

Fracture mechanics is an amalgamation of the fields of design, material science, and fabrication techniques. These interact to address the question of the suitability of a certain component for a specific purpose, Herzberg {2}. The need for the study of fatigue and fracture mechanics was first recognized after a series of disastrous railway accidents in Great Britain in the mid 19th century, Broek {3}. An average of over 200 people were killed each year in train wrecks caused by the unexplained failure of rails, wheels and axles between 1860 and 1870. Many more people were injured when iron and steel bridges collapsed without warning. Some of the bridges were almost new while others had been in service for some time with no detectable problems. Other unexplained accidents included boiler explosions and ruptures of large storage tanks such as the Boston molasses tank. In January of 1919, this huge steel tank burst and 12 persons and several horses were drowned in over eight million litres of molasses. These types of failures confounded the best engineers of the time, Rolfe and Barsom {4}.

These failures of fairly new, dynamically loaded steel and cast iron structures continued unexplained until the Second World War. At this time, almost one third out

of over two thousand welded Liberty cargo ships suffered serious structural failures shortly after being built. There have been many more cases of fracture over the years as new high strength materials have come into use. The Liberty ship mishaps and other failures of ships and barges caused Lloyds of London and the American Bureau of Shipping to begin to regulate the design and steel composition of welded ships in the late 1940s. This sparked interest in the phenomenon of the fracture of materials. Since then, several major bridge collapses and problems with various aircraft programs have expanded the field to include other materials such as titanium and aluminum.

1.2 The Canadian Offshore Research Effort

The current research activity in Canada, and in other energy producing countries, into the fracture mechanics of welded steel structures is directed towards improving the reliability of the properties of weldments and finding new ways of predicting the life of welded steel structures. Clearly, if the fatigue life of a structure can be predicted more accurately and reliably, the need for costly and dangerous inspections will be decreased. Repairs and inspections can be carried out on a scheduled basis rather than in an emergency and the chances of a disastrous accident will be decreased.

Because of the inherent limitations of the S-N method, builders and regulatory authorities have begun to investigate a more realistic approach to fatigue design which does account for pre-existing flaws in the structure. This approach is known as Linear Elastic Fracture Mechanics or LEFM. The early leaders in the field of offshore fracture mechanics were the European Economic Community members. The British, Norwegians,

Dutch and the Germans have had several very large research programs in place for a number of years {5}-{9}. For over ten years the Norwegian certification and regulatory agency, Det Norske Veritas (DNV), has permitted designers to use either the S-N or the fracture mechanics approach when designing offshore structures, DNV being the first body to allow the use of either method, {10} and {11}.

The Canadian research program into the stress analysis and fracture mechanics of offshore welded steel structures has been operating in collaboration with the European offshore research efforts for approximately six and a half years. A study found that the Canadian effort has made some valuable contributions to this important field in the relatively short time that it has been in operation {12}. Under the auspices of MTL-CANMET, the Materials Technology Laboratory of the Department of Energy Mines and Resources, the Canadian program has been conducted at Memorial University in St. John's, Newfoundland, the University of Waterloo in Waterloo, Ontario, and Carleton University in Ottawa, along with MTL in Ottawa.

The stress analysis of the weld toe region and the influence of weld geometry and operating environment on the initiation and growth of fatigue cracks, both in air and chilled seawater, has been the chief aim of the Canadian program, {13} and {14}. There are several different types of specimens used in the experimental program. The most complex and expensive is the full scale tubular joint which costs approximately \$70K per specimen. The high cost of the full tubular specimen has prompted the development of a number of other simpler specimens such as the pipe-plate and the tee-plate which cost approximately \$12K and \$2K each respectively.

The Canadian program has studied the effect of plate thickness and corrosion and, more recently, weld profile as per AWS 01.1 on the fatigue life of welded offshore structures and in the fatigue behaviour of stiffened tubular joints, {15}-{17}. From the test program carried out using welded T-plate joint specimens, it has been shown that the development of fatigue cracks can be considered as three distinct regimes. These are illustrated in Figure 1.2. It has been found that the initiation phase is rather short for a weldment, while the crack coalescence phase represents a significant portion of the overall life of a fatigue-loaded structure.

The Canadian institutions have made an extensive study of the processes that are at work in these two regions. The effort at Memorial University has been directed toward the development of specifications for assessing the safe fatigue life of welded offshore joints in the severe Canadian operating conditions. Some work has also been done with T-plate specimens loaded in tension. The University of Waterloo has carried out experimental work on fatigue testing of T-plate and pipe-plate joints and has conducted a program of corrosion fatigue testing of large size, internally stiffened, tubular joint specimens loaded in both in-plane and out-of-plane bending, Burns and Vosikovsky {18}.

The Canadian offshore structures research program has been reviewed recently in two papers, {19}, {20}. One of the chief findings, {19}, is that cathodic protection does not appear to restore the fatigue life of joints in seawater to that of the same joint in air. This is a very serious matter since several design codes allow the life of marine structures to be assumed to equal the life in air if cathodic protection is provided. The effect of section thickness was summarized, {20}, and it was confirmed that increasing thickness

results in a lowering of the fatigue life expectancy of a structure unless some attempt is made to improve the weld profile. This improvement depends on the workmanship of the builder and is a very difficult quantity to control in actual construction.

Researchers at Carleton University have made significant contributions to the field. Stress intensity factors (SIF) and stress concentration factors (SCF) in the weld toe region have been determined with the use of a large finite element package, Bell {21}, {26}, {27} and Bell and Kirkhope {22-25}. A computer simulation of fatigue crack growth using several different models has been developed {27} and has given accurate predictions of growth rates and shape development, Bain {28}, and McFadyen {29}. Other work includes studies on the stress analysis of the weld toe region and the stress field around the crack tip. An important part of this work has been an effort to improve the accuracy of the crack measurement system employed in the experimental work, Frise and Bell {30}. The recent work at Carleton has centred on the post-initiation or crack coalescence phase of life. The process of crack coalescence occurs after the cracks have initiated but before large scale growth in the through-thickness direction has taken place.

1.3 Crack Coalescence

Crack coalescence is the process of shape development where several small semi-circular cracks gradually lengthen and become elliptical. This continues until the cracks join or coalesce and start to grow as a single crack. The stable crack growth proceeds until the critical stress intensity factor is reached, at which time sudden failure or net section yielding takes place. The stable crack growth phase is well understood, and is

treated in several standard texts on fracture mechanics, {2} - {4}, {31}, and {32}. The growth of cracks in this regime is fairly well predicted by equations of the well known Paris Law type.

The crack coalescence phase is not nearly as well understood and, since it encompasses a significant portion of the total fatigue life (see Figure 1.2), it is important that more knowledge in this area be gained. A good deal of work has been done at MTL CANMET and Carleton University and the research continues. Studies have investigated the effect of the weld undercut {28} and plate thickness on fatigue life and crack shape development {13}, {15}, and {20}.

The present research is in support of this effort to better understand the process of crack growth, coalescence, and crack shape development in notch regions. This work is a study of fatigue cracks occurring at the toe of the fillet weld that forms the basis of almost all offshore structural joints. This region can be considered as a wide open notch with an included angle of 135° , Berge {33}.

To carry out this study, a research program of experimental and analytical work has been carried out. A program of fatigue tests on notched flat plate specimens has been done using a closed loop hydraulic fatigue testing machine. The test specimens were made from a Canadian-made steel that is a candidate steel for use in offshore construction. This work is described in Chapter 4. A brief review of the concepts of linear elastic fracture mechanics and a survey of the literature on the notch effect, shape development of cracks, and the phenomenon of crack coalescence is included in Chapter 2. The crack shape development was monitored with the Alternating Current Potential

Drop (ACPD) method. This technique has been shown to give accurate and repeatable crack depth data if it is carefully applied. One of the key assumptions used in the ACPD method is that the induced electrical field is uniform on the surface of the specimen. This was investigated and the resulting field measurements, and the conclusions drawn from them, are presented along with an explanation of the potential drop method of crack measurement in Chapter 3.

The analytical program has involved a finite element study of the specimen to assess the stress field in the vicinity of the notch root. The finite element analyses, the results of the fatigue testing program and a series of life predictions and simulations of crack growth are presented in Chapter 5.

1.4 Objectives of the Present Program

In summary, the objectives of the present program are:

1. Investigate the coalescence of arrays of fatigue cracks growing from notches.
2. Investigate shape development and growth rate of fatigue cracks growing from notches.
3. Investigate and improve the implementation of the ACPD crack measurement equipment.
4. To use the results of the above investigations in a life prediction model.

Figure 1.1 Various Types of Offshore Welded Structures

Figure 1.2 Three Phases in the Life of a Fatigue Crack.

Chapter 2 Linear Elastic Fracture Mechanics

2.0 Introduction

This chapter will review the literature describing the fundamental concepts of fracture mechanics and will present the most important equations used in the field. The evaluation of stress intensity factors will be discussed along with their use in the prediction of crack growth. A final section will describe the process of crack coalescence and will review previous work in this area.

2.1 Linear Elastic Fracture Mechanics - Basic Concepts

Fracture mechanics is based on the premise that a flaw is present in a structure or component. The flaw could occur at a discontinuity in the microstructure, a poorly dressed weld, or it could be a visible crack caused by service loading or faulty

manufacturing. This concept of an existing flaw represents a fundamental difference between fracture mechanics and classical fatigue wherein the body is assumed to have no flaws at the outset of life.

The life of a structure can be divided into two phases. These are:

- i) The Flaw Initiation Phase - cracks form or initiate at points in the structure where fabrication or microstructure defects provide favourable conditions.
- ii) The Flaw Propagation Phase - microscopic cracks grow until they encounter and join with other flaws. These larger cracks continue this process of growth and coalescence until a critical stress-strain state is reached at the crack tip. At this point fracture will occur, either in a brittle or in a ductile fashion depending on the degree of constraint (ie. whether plane stress or plane strain conditions exist).

The material properties, which themselves are a function of temperature, also play an important role in determining the failure mode.

The relative proportion of the total life that a structure spends in each of these regimes is very important to the utility and safety of the structure. If the propagation portion of the total life is very short, then the structure will fail very shortly after a flaw initiates. Failure may even occur before the flaw is detected. Clearly, this situation is very dangerous.

A paper by Sih {34} serves as a good introduction to the study of the crack tip. He discussed the microstructure aspects of the crack tip region, pointing out that inhomogeneity in the grain structure can cause the crack to be arrested temporarily or deflected from its original path. The model proposed by Sih characterizes the crack tip region in one of three categories depending on the relative size of the crack tip radius, versus grain size. The three categories are illustrated in Figure 2.1.

The three possible geometric relationships between the applied loads and the orientation of the crack are illustrated in Figure 2.2. These are known as the three fracture modes. The most common of these is mode I, or the opening mode, because the applied stress tends to open the crack in a way similar to the opening of a door. Modes II and III are known as the sliding mode and the tearing mode respectively. These two modes involve shearing deformation either parallel to, or perpendicular to, the crack surfaces. The stress and displacement conditions at the crack tip in any situation may be considered as a combination of one or more of these three modes. Modes II and III are much less common than mode I but may be treated in a similar way.

Conditions at the tip of the flaw are described by the Stress Intensity Factor or SIF. For a mode I crack, it is given the symbol K_I and has dimensions of *stress* $\times$ *length*^{1/2}. The stress intensity factor is a function of the geometry of the flaw and the loading. The stress at the flaw tip will be higher than the nominal stress in the structure due to the stress concentration caused by the flaw. The variation of stress with position for a mode I crack is shown in Figure 2.3 and is described by equations 2.1 which were developed by Westergaard {35}.

$$\begin{aligned}\sigma_x &= K_I (2\pi r)^{-1/2} \cos \frac{\theta}{2} \left(1 - \sin \frac{\theta}{2} \sin 3\frac{\theta}{2} \right) + \dots \\ \sigma_y &= K_I (2\pi r)^{-1/2} \cos \frac{\theta}{2} \left(1 + \sin \frac{\theta}{2} \sin 3\frac{\theta}{2} \right) + \dots \\ \tau_{xy} &= K_I (2\pi r)^{-1/2} \cos \frac{\theta}{2} \sin \frac{\theta}{2} \cos 3\frac{\theta}{2} + \dots\end{aligned}\tag{2.1}$$

$$\begin{aligned}
 \text{also } \sigma_z = \tau_{xz} = \tau_{yz} = 0 & \quad (\text{for plane stress}) \\
 \sigma_z = \mu (\sigma_x + \sigma_y) & \\
 \text{and } \tau_{xz} = \tau_{yz} = 0 & \quad (\text{for plane strain})
 \end{aligned}$$

where K_I is the mode I stress intensity factor, and r and θ are polar coordinates as shown in Figure 2.3. Note that while the distribution of stress at the crack tip is a function of location, the magnitude is purely a function of the stress intensity factor. This gives rise to one of the most important tenets of fracture mechanics; that the stress intensity factor completely describes the stress field at the crack tip.

An important extension of this idea is the concept of similitude of one crack tip to another if the stress intensity factors of the two are equal. One consequence of similitude is that two cracks will grow at the same rate in a given material as long as the stress intensity factors at the crack tips are equal. Thus, the stress intensity factor links the local response at a crack tip and global variables such as applied stress, and geometry of the cracked body. It can be seen from equations 2.1 that the crack tip stress is a singularity of the form $r^{-1/2}$. The maximum value of stress that can occur in the crack tip region is equal to the yield strength of the material. This results in a zone of plasticity at the tip as indicated in Figure 2.1. As long as the constraint present in the cracked body is sufficient, this plastic zone is small compared to the length of the crack. In this case, plane strain conditions can be considered to apply and the laws of linear elastic fracture mechanics (LEFM) are valid.

A related condition for LEFM to be valid is that the crack must be relatively large

compared to the microstructure grain size of the cracked material. Most authors suggest that the crack should be at least five grain diameters in length for this continuum assumption to be valid. For the type of steel employed in the present research, this would imply a minimum crack length of at least 0.3 mm, which is close to the lower limit of detectable crack size with the available equipment. The SIF when fracture occurs is called the critical stress intensity factor or K_{Ic} . This critical value is a material property and can be determined by a fracture toughness test method known as the ASTM E399-74 test.

2.2 The Stress Intensity Factor - K_I

Once the value of K_{Ic} is determined for the material, it is possible to determine the flaw size that will result in failure of the structure. This is done with the aid of equations of the form of equation 2.2

$$K_I = Y \sigma_{nom} \sqrt{\pi a} \quad (2.2)$$

where a is the flaw size, σ_{nom} is the nominal applied stress,

and Y is a coefficient that depends on the loading, and

the geometry of the cracked body and the flaw itself.

This equation incorporates design factors in the form of the nominal stress, and the geometric coefficients and fabrication considerations in the form of the flaw size. Whenever the flaw size, stress, and geometric factor combine to produce $K_I = K_{Ic}$, the structure will be subject to fracture.

The determination of K_I for various geometries and loading situations is a major

field of research in itself. Several different numerical, analytical and experimental techniques are used to find this quantity. There are several handbooks in which SIF values are presented for many simple geometries and loading situations, {36}-{38}. Usually these solutions have been determined by rigorous mathematical means and are exact solutions for the situations presented. Exact solutions for more complex geometries are not easily obtained, however, and therefore several different numerical techniques are employed. The two most commonly used methods are finite element analysis and the weight function method. These are described in subsequent sections.

2.2.1 Finite Element Analysis

The finite element method has gained widespread acceptance as a useful tool for modelling complex geometries and stress states. This is because the method permits any geometry and loading to be analyzed with a very high degree of accuracy.

As was noted above, the stress field at a crack tip forms a singularity of the form $r^{-1/2}$. In order to accurately model this singularity, several special crack tip elements have been developed. One commonly used crack tip element is the quarter-point shifted node isoparametric element developed by Henshell and Shaw {39}. Another type of singularity element was developed by Barsoum {40}. Each of these generates the correct singularity at the nearest corner node. Bell {21}, {26}, {27} and Bell and Kirkhope {22-25} have performed an extensive series of stress intensity factor determinations using a large commercial finite element package and these singularity elements. The geometry modelled was the toe of a weld such as would be found in a joint on an offshore

structure.

There are several methods that may be used to determine stress intensity factors from an FEM model. The simplest and most effective method, and that used by Bell and many other researchers in this field, is to substitute the displacement data obtained from the FEM analysis into classical equations that apply at the crack tip such as the Westergaard equations (equations 2.1). The output of a finite element model of a crack tip region is in the form of nodal displacements and so Bell found it convenient to use the displacement equations to find SIF values for edge and semi-elliptical weld toe cracks.

The analyses done by Bell et al have been incorporated into a fatigue life prediction computer program which will be described below. They have also been implemented in the data reduction software used in the present experimental program to determine analytical stress intensity factors based on the crack shape development models described in Chapter 5 of this thesis.

One disadvantage of using the finite element method to find stress intensity factors is that the analyses are very expensive and the data preparation is an enormously complex problem especially if a three dimensional analysis is to be done. Also, the data from a single model is valid for only one crack depth and shape. This means that to develop a usable set of stress intensity factor values for a given geometry, several separate finite element models would have to be analyzed. Several modern packages, including the one used by Bell et al, have a substructuring capability and automatic mesh generation facilities, but the method is still expensive and needs a great deal of computing power.

2.2.2 Weight Function Methods

An alternative to the finite element analysis method is the weight function method developed by Bueckner {41} and Rice {42}. This method is based on the premise that a relationship exists between the stress and displacement fields of an uncracked body under a certain loading system and the equivalent quantities for the same body in a cracked condition. For a two dimensional body this relationship can be stated as

$$K = \int_0^a \sigma(x) m(x,a) dx \quad (2.3)$$

where K is the stress intensity factor to be determined,

$\sigma(x)$ is the loading corresponding to an uncracked configuration,

x is the distance along a crack of length a ,

and $m(x,a)$ is the functional relationship known as a weight function.

Another way of stating Bueckner's principle is that the SIF at the tip of a crack in a loaded component is the same as the SIF for that component with the uncracked stress distribution applied on the crack face. This uncracked stress distribution could be found from a single finite element analysis of the undamaged part and must be determined along the same plane as the crack.

In the same way that the classical stress intensity factor is a product of modifying coefficients applied to a reference SIF, Bueckner attempted to generalize weight functions by using a reference stress intensity factor K_R resulting from a reference loading system $\sigma_R(x)$. The resulting weight function formulation is then

$$m(x,a) = \frac{H \partial u(x,a)}{K_R \partial a} \quad (2.4)$$

where H is equal to Young's modulus for plane stress

or $\frac{E}{(1 - \mu^2)}$ for plane strain,

and $u(x,a)$ is the crack opening displacement field for the reference loading system.

This requirement for the crack opening displacement points out one of the difficulties with the Bueckner approach. This data is not generally as easy to obtain as stress field data, and so a modification developed by Petroski and Achenbach {43} greatly enhanced the usefulness of the weight function method. They proposed an equation that approximates the crack opening displacement for a reference crack geometry and loading for which the SIF is known. This development was used later by Niu and Glinka as described below.

Albrecht and Yamada {44} developed a method that deals with determining SIF values in the region of stress concentrations. They propose a geometry correction factor derived from consideration of the SIF for a single central crack in a wide plate subjected to equal pairs of opening forces

$$K = \frac{2 P a}{\sqrt{\pi a} (a^2 + b^2)^{1/2}} \quad (2.5)$$

where a is the crack depth, and the equal splitting forces are applied at a distance $x = \pm b$ from the centre of the elliptical central crack. This method has been found to work reasonably well but it has one limitation in that it can only be used where the nominal stress in the component is uniform, ie. pure tension.

Albrecht and Yamada specifically state that the method is not applicable to the case where a component is loaded in bending. Therefore, while the method may be very useful for the case of bridges for which it was originally developed, its usefulness for offshore platforms, where bending loads are common, is somewhat limited. Skorupa et al {45} reviewed several methods for approximating SIF values at weld toes and concluded that, indeed, the Albrecht and Yamada method was the most suitable for tension loaded joints.

Niu and Glinka {46}, {47}, {49} and Niu et al {48} have used the Petroski and Achenbach method of approximating the crack opening displacement from the stress distribution. They have derived a weight function for fatigue cracks at weld toes that is applicable to several different loading situations including the important cases of three-point and four-point bending. This has been accomplished by using super-position of known stress distributions and stress intensity factors for simple bodies with the uncracked stress distribution at the weld toe. This approach has been used to investigate the effects of weld geometry on SIF values {48}, {49} and has been found to give SIF values very close to those obtained by using FEM by Bell {21}, {26}, and {27} and Bell and Kirkhope {22-25}.

2.3 Fatigue Life of Engineering Structures

An extensive study by Verreman et al {50} of the various life prediction methods in use concluded that current models of crack initiation and growth are not adequate to predict structural life in many real situations. This is because traditional S-N fatigue curves assume that the structure has not suffered any damage. It has been well established, however that virtually all welded steel structures either have cracks at the outset of their service life or will develop cracks shortly thereafter. These cracks completely invalidate a traditional S-N fatigue analysis.

The field of life estimation is still developing and as yet, no definitive method of calculating the life of welded steel structures has demonstrated a clear superiority over the others. Most of the research to date has involved a program of analytical work and experimental tests that are used to validate the analysis.

2.3.1 Factors Affecting Crack Growth Rates

Aside from the obvious factors that can affect crack growth rates such as microstructure defects and poorly made welds, numerous studies have been made into other causes of growth rate variations. Vosikovsky et al {13} studied the effect of plate thickness on the fatigue life of welded T-joints. They concluded that, at the same stress, fatigue life decreases with increasing plate thickness. Their study included fatigue testing in an MTS closed loop hydraulic fatigue test rig, and accompanying FEM analysis for assessment of SIF. The proportion of the total life spent in the initiation phase was 14-23%. The total life of the thicker specimens was markedly less than that of the

thinner specimens in all three stress ranges studied. The decrease in life between the 16 mm specimen and the 103 mm specimen was a factor of over 7.5.

Verreman et al {50} identified several factors that can cause errors when using traditional methods. Besides the expected microstructure defects, crack growth from the root of a notch plastic zone, and residual stress left by welding were identified as factors. These real-world factors can lead to unconservative life estimates if classical S-N or LEFM approaches are used. The initiation life of defects in the vicinity of a notch is very short. This is of vital interest to the offshore structural designer because each welded joint can be considered as a wide open notch. Thus, each offshore platform has thousands of prime crack initiation sites. Verreman discussed the effect of the stress ratio, R , on crack growth rates. He states that lower values of R produce more anomalous short crack behaviour. This short crack effect is becoming a topic of great interest in the field of fracture mechanics, Leis et al {51}, Taylor {6}, El Haddad and Dowling {52}.

Until recently, most researchers simply ignored the initiation life of cracks within the weld toe region because it was felt that the proportion of the total life spent in initiating these cracks was negligible compared to the total life. The work of Vosikovsky et al, Verreman and others have shown that this is not entirely true. The error incurred in making this assumption may not be as small as was originally thought, particularly when weld improvement techniques are employed.

2.4 Fatigue Crack Growth

The cyclic loading of a structure can cause a flaw or crack present in the structure

to open and close. Thus, the SIF at the tip of the crack varies over time as a function of the amplitude of load variations. This repeated opening and closing of the flaw causes the metal at the crack tip to fail by fatigue. This results in the growth of the crack according to an equation of the form of equation 2.6

$$\frac{da}{dN} = f(\Delta K) \quad (2.6)$$

where da/dN is the crack growth rate per cycle,

ΔK is the SIF range, ie. $(K_{MAX} - K_{MIN})$.

The relationship of the crack growth rate, da/dN , and the SIF range, ΔK , is usually seen to follow a sigmoidal shape curve as shown in Figure 2.4. There is a threshold at low values of ΔK below which cracks do not appear to grow, while at high values of ΔK the growth rates increase dramatically until the critical SIF is reached and fracture occurs. The middle region is of most interest in an engineering sense since critical components of most fatigue loaded structures such as oil platforms and aircraft operate in this regime. Crack growth rates in this region can be described by the Paris-Erdogan [53] law,

$$\frac{da}{dN} = C (\Delta K)^m \quad (2.7)$$

where C and m are empirically determined material parameters.

The Paris Law has been found to describe the growth of fatigue cracks very well. Integration of equation 2.7 between the limits of the minimum detectable crack size and the critical crack size, as found by equation 2.2, will give a reasonable estimate of the number of load cycles required to grow the crack to a_{CRIT} when failure will occur.

Many researchers have found that equation 2.7 does not accurately describe the growth rate of cracks that are short in relation to the size of their plastic zones. This is known as the short crack anomaly. It is also inadequate when the crack is long, such as when it is near the critical size. The Paris law is also valid for only one value of R , the stress ratio, which is defined as $R = \frac{\sigma_{MIN}}{\sigma_{MAX}}$. Different values of R generally produce a family of parallel lines when plotted as da/dN versus ΔK as in Figure 2.4. To account for these problems, many modifications of the Paris-Erdogan law have been proposed over the last 25 years.

These have been reviewed by Verreman et al {50}, Dijkstra and Snijder {54}, and El Haddad and Topper {55}. One of the most commonly used growth laws was developed by Forman et al {56} to account for the effect of varying values of R

$$\frac{da}{dN} = \frac{C \Delta K^m}{(1 - R) K_{IC} - \Delta K} \quad (2.8)$$

The threshold stress intensity factor, K_{Th} , below which crack growth would not occur was accounted for by Klesnil and Lukas {57}

$$\frac{da}{dN} = C (\Delta K - \Delta K_{Th})^m \quad (2.9)$$

where ΔK_{Th} = the threshold SIF range.

Schutz {58} extended this to incorporate the effect of the stress ratio by modifying the Forman expression (equation 2.8)

$$\frac{da}{dN} = \frac{C(\Delta K^m - \Delta K_{Th}^m)}{(1 - R) K_{IC} - K_{MAX}} \quad (2.10)$$

An important disadvantage of equations 2.9 and 2.10 is that they require determination of the threshold stress intensity factor for the material. This requires a great many separate tests to determine and is therefore both time consuming and expensive. Several other expressions exist, many of which are much more complicated than those presented above. Broek {3} pointed out that there is no rational basis for selecting one over another except that of a good fit with the available data.

All of the expressions have been found to give good predictions under a given set of circumstances. The material parameters C and m are determined empirically by fatigue testing simple specimens for which the stress intensity factor is known. The crack growth rates of these specimens are calculated from experimental data, and values for C and m are determined from one of the growth laws. It is important to note that, in general, the values of C and m are not interchangeable among the various growth laws but are valid only for the law from which they were obtained, Broek {31}.

An important paper by several major authors, Leis et al {51}, has shown that these modified forms of the Paris law are not really valid. They suggest that because of a lack of similitude between the tip of a short versus a long crack, there is no physical basis for supposing that a single expression could describe both situations. Therefore, the two must be treated as separate problems.

2.5 Crack Shape Development

The change of the shape of fatigue cracks as the life of the specimen is consumed has been a subject of interest for some time. The experimental monitoring of crack shape

evolution was a major aim of the present program. Much of the work in this field has been done by producing a series of beachmarks on the crack face at known cycle counts during the fatigue test. This is achieved by changing the amplitude and frequency of the load cycling for a number of cycles. The crack will grow a small amount and will have a different appearance during this time.

More recent research work has determined crack shape evolution by performing a series of potential drop measurements along the length of the crack. The various crack depths are then plotted as a function of position and a representation of the crack shape is produced. This is the procedure adopted by Vosikovsky et al {14} in a study of the effect of plate thickness on the propagation and shape evolution of fatigue cracks in welded T-Joint specimens.

They assumed that the fatigue cracks are initially semi-circular and that as fatigue cycling progresses, the shape evolves to a semi-elliptical form (see Figure 2.5). It was found that the evolution of crack shape could be described by a simple relationship

$$\frac{a}{c} = e^{-ka} \quad (2.11)$$

where a/c is the aspect ratio of the crack as defined in Figure 2.5, and k is a factor that describes the relationship. This is known as a forcing function and it is discussed further in the section on crack coalescence. The parameter k has been found to be a function of both the stress range of the fatigue test and the thickness of the plate, with the former having the strongest influence. Values of about $k = 0.2$ for 100 MPa stress ranges and $k = 0.5-1.2$ for 200 MPa stress ranges were found to fit the data very well. Bain {28}

found that equation 2.11 was valid from his 'tee' plate tests as well. Vosikovsky found that the number of fatigue cracks that initiated along the fracture surface varied according to the plate thickness and the stress range level. As cycling progressed, the aspect ratio (a/c) of the separate cracks decreased until the cracks joined and the cracks continued to grow as a single straight fronted crack or a crack with a very low aspect ratio. This process of joining is known as fatigue crack coalescence.

Topp and Dover {59} used an automated fatigue testing facility located at University College at the University of London, England. The monitoring of crack shape evolution is performed by the alternating current potential drop (ACPD) method of crack sizing that is described fully in Chapter 3.

Aboutorabi and Cowling {60} have applied the ACPD method to the determination of stress intensity factors for surface cracks. Fatigue crack growth data obtained experimentally, along with published values of C and m , the Paris growth constants, are used to evaluate the Paris law. They have been able to determine the variation of SIF along the crack front. This method will be employed in the present research.

The crack shape development model used in the present study for the initial series of specimens without starter cracks is the same as that developed by Vosikovsky et al. For the double crack specimens, a new model has been developed which is described in detail in Chapter 5 of this thesis.

2.6 Coalescence of Fatigue Cracks

The coalescence of fatigue cracks is a very complex physical process and is not

completely understood at the present time. Relatively few studies of crack coalescence in steels have been published to date. The research work that has been published has noted that, in general, existing design codes do not address crack coalescence in a realistic manner.

The design codes that attempt to provide guidance on the treatment of crack coalescence are the ASME Code Section XI {61} and the British Standards Institution PD6493 {62}. The rules for treating adjacent fatigue cracks in these documents note that the two smaller cracks will join and assume a shape defined by a bounding semi-ellipse. The depth of this newly formed crack is assumed to be equal to the depth of the deeper of the two pre-existing cracks and the length is assumed to be equal to the sum of the their lengths. This process is known as recharacterization. Rules are also proposed to account for accelerated growth in the length direction as the two crack tips approach each other, this being known as interaction. Much of the research on coalescence to date has been directed towards studying the two phenomena, recharacterization and interaction.

Iida and Kawahara {63} compared the results from their experimental study of the coalescence of multiple crack arrays and found that the ASME section XI guide lines did not provide a very good description of coalescence. They noted that the interaction between adjacent cracks was not apparent and that the prescribed shape development predictions of {61} were not observed in experimental specimens.

An experimental study on crack coalescence was carried out by Tanaka et al {64}. They used rotating beam fatigue test specimens with three small radial holes drilled at the root of the notch to simulate starter cracks. The statistical nature of the fatigue life

reduction due to the presence of the starter cracks was observed as well as the lengths of the fatigue cracks emanating from the holes after a given number of cycles. The results of the study were that a significant decrease in fatigue life occurred as a result of the presence of multiple fatigue cracks. This work was noteworthy at the time (1985) as it was one of the few published studies where crack coalescence was studied experimentally. This paper did not, however, address the problem of multiple initiation of cracks in a weld or notch type geometry which is the subject of the present work.

An early work on the multiple crack problem is due to Vialaton et al {65}. They studied the stress field in a plate with two coplanar cracks of equal length separated by a given distance. It was found that if the cracks are separated by a distance equal to or greater than the length of the crack, then the effects of crack interaction could be neglected with only a small error. It should be noted that no experimental work was undertaken in this study. Chang {66} studied the effects of crack-crack interaction using real microstructure data obtained from actual tests. The physical data was incorporated into a Monte Carlo simulation that was designed to predict the coalescence of multiple fatigue cracks. Again, however, no experimental work was performed.

The interaction of semi-elliptical flaws was studied by Murakami and Nemat-Nasser {67}. They noted the difficulty in studying bodies with more than one crack and, more particularly, the interaction between the cracks. Their studies showed that when a crack encounters a pre-existing small flaw such as a minute scratch, the crack tends to grow toward the flaw. The crack separation distance-interaction effect found by Vialaton was confirmed in this study. Moreover, the Murakami and Nemat-Nasser paper

investigated the case of a small crack interacting with a large crack and reported a similar separation distance effect.

While the present research is directed towards fatigue crack coalescence in steel, it is interesting to note several groups of researchers have studied coalescence in other materials. McComb et al {68} investigated coalescence in polycarbonate specimens in an attempt to determine a method of analytically predicting the coalescence of two cracks growing from the inside of a hole. The specimens employed were rather small in the dimension parallel to the bore of the hole. This gave rise to a significant edge effect and would therefore limit the applicability of this work to offshore fatigue since the edge effect is not found in tubular joints. Offshore researchers expend a good deal of effort to minimize this edge effect in experimental work.

Yoda {69} studied crack coalescence in glass. His measurements of crack growth velocity in the length direction, dc/dt , indicate that there is crack-crack interaction. The resulting life is thus shortened by the presence of two small cracks when compared to the life with one large crack. It must be noted, however, that glass is much more brittle than structural steels and this interaction effect is not always observed in steel before the cracks tips touch.

Hoshide et al {70} studied fatigue crack coalescence in copper specimens. They noted that crack growth accelerates in the c direction (at the surface), just before coalescence and thereafter decelerates. They felt justified in assuming that the net effect of the two small cracks on the growth at the surface would be cancelled. Their simulation of coalescence simply assumes the presence of a single long crack once the

two small crack tips are near to each other. The applicability of this work to crack coalescence in offshore steels is unknown, but many of the principles presented by Hoshide are of interest.

Twaddle and Hancock {71} found that the interaction of two closely adjacent cracks before coalescence is indeed limited. They found, however, that after the crack tips actually touch, the SIF in the cusp region is greatly magnified as evidenced by very high growth rates. The coalescing cracks quickly assume the shape of a larger single crack. The recharacterized crack is semi-elliptical in shape and has a much lower aspect ratio than the two cracks that formed it.

Snijder et al {72} studied crack growth in tee-plate and tubular X-joints including a consideration of multiple initiation of cracks at the weld toe. They evaluated several different methods of predicting crack growth in welded joints including three different ways of accounting for multiple initiation and coalescence and concluded that the best model is one that is specifically developed for the geometry under consideration. This comment was found to apply specifically to the forcing function method of modelling aspect ratio evolution proposed by Vosikovsky et al {14}. The forcing function was not found to be generally applicable to geometries other than that for which it was developed.

Kishimoto et al {73}, and Soboyejo et al {74}, conducted an experimental and analytical study of crack coalescence in a pressure vessel steel. The specimens had two starter cracks produced by fatigue loading ridges on the top surface and then removing the ridges. This left the plate flat, with two semi-elliptical starter cracks. The plates were loaded in four point bending and the subsequent growth of the fatigue cracks was

monitored by the direct current potential drop (DCPD) method and beachmarking. An analytical program of computer simulation with stress intensity factors determined by the finite element method was also carried out and good agreement in predicting fatigue life with the experimental work was found. They noted the lack of significant interaction before the crack tips contacted each other, as did Twaddle and Hancock {71}. The SIF in the cusp region of the coalescing cracks was found to rise rapidly at the onset of contact and then decrease as the small cracks formed one longer crack of lower aspect ratio.

Ortegui et al {75} performed a study of fatigue crack initiation at weld toes using tee-plate specimens. They were particularly interested in characterizing the quantity and types of weld defects found at the toes of both manual and automatic welds. They noted that small micro-cracks form at these defects and then coalesce to form easily observable macro-cracks. It was found that the aspect ratio (a/c) of the micro-cracks coalescing from defects tends to increase from a relatively small value, ranging from approximately 0.1 to 0.3.

2.7 Simulation and Analytical Prediction of Crack Growth

The simulation of crack growth and shape development is a field of significant interest. This is due in part to the high cost and lengthy nature of fatigue testing programs. This is especially true of corrosion fatigue studies where the cyclic loading must be performed at frequencies close to those found in service, typically less than 1 Hz.

The computer simulation of fatigue crack growth is an application of the basic

laws of fracture mechanics outlined above. The stress intensity factor for a given specimen geometry and loading is calculated. One of the crack growth laws is then applied to this data and the value of da/dN is determined from which a new crack depth is found. The process is then repeated until a failure condition or a final crack depth is reached. The key to accuracy in these simulations is to find a stress intensity factor solution that adequately describes the physical reality of the actual prototype.

2.7.1 Damage Summation

In order to better simulate the actual operating conditions of offshore structures, a damage summation law known as the Palmgren-Miner cumulative damage law is commonly used. The law is a way of assessing the damage inflicted upon the structure at a certain stress level and adding it to the damage done at other stress levels to determine the total accumulated damage to the structure. In this way, it is possible to derive meaningful results from tests done at many different stress levels. This could occur when a variable load amplitude test is carried out using, for instance, the common load sequence developed by Schutz {7}. The law takes the form

$$\sum_{i=1}^j \frac{n_i}{N_i} = 1 \quad (2.12)$$

where j = the number of stress levels under consideration,

σ_i = the stress at the i_{th} level,

n_i = the number of cycles applied at σ_i ,

and N_i = the fatigue life if the stress amplitude remains at σ_i .

Note that the law is simply a means of recording what portion of the fatigue life is consumed at each stress level. This procedure is somewhat unrealistic because it does not account for the differences that will occur due to loads being applied in a different order. For example, if high stress levels are applied to the structure before lower ones, a compressive stress field will exist at the crack tip. This will inhibit further crack growth under subsequent less severe cycles and will cause the fatigue life to be lengthened. The Palmgren-Miner law does not account for this effect.

Therefore, some form of statistical approach or randomness must be used in the application of the load sequences. Ellyin and Falkinlede {76} developed a statistical method of applying the Palmgren-Miner law that addresses this difficulty. Their approach is based on a weakest-link theory in which the material ahead of the crack tip is discretized into elements of randomly varying strength. Each of these is assumed to accumulate damage over some number of cycles. A computer program statistically determines which one of these elements will fail first. The position and strength of this weakest-link element determines the onset and extent of crack advance. This type of model seems to be what Sih was proposing in his 1983 paper {34}. Comparisons of this procedure with experimental work show good agreement.

Bell {27} has used the program FATCRACKS which is presently available at Carleton University. This program is capable of predicting the growth and shape evolution of arrays of fatigue cracks including the simulation of the coalescence phenomenon described in section 2.6.

FATCRACKS has been found to give accurate predictions of growth for several

different geometries using one of several crack propagation and shape evolution laws including that developed by Vosikovsky et al {13}, {14}. The program is run by generating a data file consisting of a set of parameters that describe the depth and shape of an initial flaw, the stress range and loading to be simulated and several other parameters related to the selected crack growth law. The program is very flexible in its treatment of these growth laws. For instance, it is possible to select different values of the Paris law constants C and m of equation 2.7 for the deepest point of the crack and the surface breaking point. This is useful because it has been noted that the growth rates at these two points are often not described by the same values of these constants.

FATCRACKS will be used in the current research program to model the fatigue life of the vee notched specimens.

Figure 2.1 The Sih Crack Tip Model.

- (a) Crack tip in a grain $p \ll D$
- (b) Crack tip among grains $p \approx D$
- (c) Crack tip in homogeneous material $p \gg D$

r_0 = plastic zone size

p = crack tip radius

D = average grain size

Mode I

Mode II

Mode III

Figure 2.2 The Three Modes of Fracture

Figure 2.3 The Elastic Stress Field in the Vicinity of a Crack. (Ref. 1)

Figure 2.4 The Growth of a Fatigue Crack

Figure 2.5 The Evolution of Crack Shape From Semi-Circular to Semi-Elliptical.

Chapter 3 Experimental Crack Measurement

3.0 Introduction

The measurement of cracks in metal bodies is not a simple task unless it is possible to break the body apart to examine the cracks first-hand. If, however, the integrity of the body is to be preserved, then some non-destructive means of measuring the crack must be used. The major techniques used for detecting and measuring cracks are:

1. Dye Penetrant
2. Ultra-Sonic Detection
3. Electrical Potential Drop

The first two methods are normally not accurate or sensitive enough for fracture mechanics research purposes although ultra-sonic detection is beginning to find application in the aircraft industry, Tomberger et al {77}. There are two electrical

potential drop methods used to monitor the growth of fatigue cracks. These are: the direct current potential drop (DCPD) method and the alternating current potential drop (ACPD) method. The ACPD method has been used for the present work, but both methods will be briefly reviewed below.

3.1 Electrical Potential Drop Techniques

3.1.1 The D.C. Potential Drop Method

The earliest potential drop crack measurement techniques used DC equipment operating with field currents of the order of tens of amperes, Wei and Brazill {78}. The method was used in an investigation of hydrogen embrittlement of notched round specimens, Barnett and Troiano {79}. The calibration of the measurement was recognized as a problem at this time but a satisfactory solution was not proposed. The calibration of DC electrical potential drop equipment was addressed by Johnson in 1965 {80}. He developed a theoretical relationship for the normalized potential drop across a straight fronted crack in a flat plate. Lambert {17} has found that the Johnson equation is not accurate for plate thicknesses of greater than about 8 mm. Johnson did make some important contributions to the field, however. He showed that the measurement is not sensitive to the chemical composition, or the heat treatment of the specimen, as had been previously thought.

The need for a calibration technique for potential drop crack measurements was also pointed out by Gangloff {81}. He was studying subcritical crack growth from small defects with the direct current potential drop method. A calibration curve was constructed

by using a fluid flow analogy similar to that used by Dover et al {82}. The resulting calibration curve was validated by means of optical measurements made on the specimen after failure. The results indicate that the method is accurate to within about 5% for crack sizes of less than 0.10 mm. The technique developed by Gangloff was also used by Smith and Smith {83}. They reported good accuracy with the Gangloff calibration technique.

Knott {84} proposed an entirely different method of DCPD calibration for 2-D problems. His method uses a conducting paper model of the test specimen which has a current passed through it. The flaws are simulated by cutting the paper. The resulting field is then traced out using special techniques developed at Cambridge University. He suggested that three dimensional situations could be modelled by using a bath of electrolyte in the shape of the specimen. Models of the flaws made of non-conducting material would be placed into the bath and potential measurements taken with special probes. Using this technique the influence on the electrical field of several flaw shapes and sizes can be assessed at will. Burns et al {85} at Waterloo University have used this method to calibrate a DCPD system. They achieved better than 99% accuracy when the method was compared to optical measurements on actual test specimens. Knott {84} also proposed the use of conformal mapping techniques to derive solutions of Laplace's equation which describes the distribution of potential in a conducting body.

This is done by a "panel method" approach similar to that used in fluid mechanics to solve liquid and air flow problems around complex bodies such as ships and aircraft. The lines of electrical potential are shown to be everywhere orthogonal to the lines of

constant stream function (current flow). He gives a few simple examples of the technique and points out that solutions already exist for many real problems from the fluid mechanics literature. The use of the fluid mechanics approach due to Gilbey and Pearson {86} may assist in solving 3-D problems. Knott proposes that 3-D problems could also be solved by means of the finite element numerical method. Knott mentioned the applicability of the analogue method to ACPD calibration but no further work towards this has been undertaken.

Hicks and Pickard {87} made a comparison of the traditional beachmark method of PD calibration with the paper analogue and the finite element numerical calibration as proposed by Knott. They found that for a compact tension test specimen geometry the three different methods, FEM, the analogue, and the beachmarks gave very similar calibration curves. Similar results were obtained for corner cracked test specimens. The finite element work was done by using the heat transfer analysis facilities of the various software packages and considering that the current flow is physically analogous to a heat flux.

Wei and Brazill {78} described a typical experimental calibration procedure and showed that the AC and DC methods gave similar and accurate results for a 2.25Cr-1Mo compact tension test specimen. The method used was similar to that described in section 3.4. They concluded by stating that while DC systems are simpler to build and that it is easier to eliminate unwanted stray signals, the ACPD system is inherently more sensitive. The AC system is capable of better than 0.01 mm crack length resolution, a substantial improvement (more than 30%) over a comparable DCPD system.

Watt {88} also compared a DCPD system with an AC based system. He was testing titanium single edge notch specimens and found that the AC method was more sensitive than the DC system and when calibrated with beachmarks and post test optical crack measurements, gave more accurate measurements.

3.1.2 The A.C. Potential Drop Method

The London Centre for Marine Technology at the University College of London has carried out leading research in the field of electrical crack size measurements. The development of the ACPD technique was carried out there in the 1970's by a team of researchers led by Dover and is described in a special technical publication of the ASTM {89}. This work led to the development of the commercially available Unit Inspection Crack Micro-Gauge that will be used in the present work. The AC potential drop method relies on the fact that an alternating current is transmitted only on the skin of a body and not throughout its entire volume as is the case with the DCPD Method. The thickness τ of the conducting skin is given by

$$\tau = (\mu_r \mu_0 \pi \sigma f)^{-1/2} \quad (3.1)$$

where μ_r and μ_0 are the relative specimen and free space permeability respectively,

σ is the conductivity the specimen material,

and f is the frequency of the applied field.

Experiments by Taylor in Britain have shown that this skin effect can be optimized for a given material by adjusting the frequency of the alternating current {90}.

This will produce a larger potential drop for a given input current and a more sensitive measurement of cracks in the specimen material. In general, for an infinite uniform bar, at a given frequency and input current, the voltage drop between two reference probes is a function of the probe spacing δ only, while the drop between a probe pair spanning a crack is a function of $(\delta + 2a)$ where a is the crack depth. Therefore, any voltage difference between the two probe pairs can be interpreted as a crack between the second pair of probes, see Figure 3.1.

The large currents required (> 10 amperes) in DC methods give rise to specimen heating. This heating causes the electrical resistivity of the specimen to change which makes it necessary to pulse the field current or limit the time spent with the field switched on. This makes the DC method less convenient to use than the AC method. There are several other factors that can affect the performance and calibration of potential drop fatigue crack measurements. One of these is plasticity in the region where the measurement is being made. Knott {84} points out that once a body has yielded, its resistivity increases. This effect should be accounted for in any analytical calibration of a specimen where significant plastic deformation is experienced. The actual methods for modelling this effect are not straightforward and none of the authors give much guidance on this matter.

All of the authors note the importance of eliminating stray electrical fields from the specimen as these fields can cause large errors in the measurements. The electronic sophistication of the recently developed ACPD equipment helps to discriminate between background noise and actual signal to a certain extent, but the problem still exists.

Dover states that the Crack Micro-Gauge operates at a frequency of 5 to 6 kHz and a field current of 1 to 2 amps with a sinusoidal waveform. Experience has shown that this yields a satisfactory balance between noise rejection and sensitivity. To further enhance the sensitivity of the instrument while maintaining resistance to stray fields, the Crack Micro-Gauge is equipped with a very narrow bandwidth sensing circuit and special filtering and shielding of the components inside the metal case of the instrument.

Dover et al described the use of a 1-dimensional solution (equation 3.2 below). They note that it is only valid when the uniform field assumption is true, ie. when the potential drop measurement is taken close to a straight line drawn between the two current input probes. The paper presents experimental data and shows that the errors introduced in crack measurements, if this condition is violated, can be substantial. Dover developed a 2-dimensional solution for a single semi-circular crack based on an analysis similar to that used in potential flow problems in fluid dynamics. He also derived a correction for a semi-elliptical crack shape. Bain, {28}, McFadyen {29} and others, have found that these 2-D correction factor tables do not give very accurate results especially when applied to semi-elliptical cracks and multiple crack arrays.

Inductance effects caused by the probe loop area and the cross-sectional area of the crack itself can also affect ACPD measurements. Mirshekar-Syahkal and Collins {91} investigated this problem and developed methods of predicting the effects of inductance. They proposed ways of compensating for it when interpreting ACPD data. Dover et al {92} also discuss the unwanted induction effects caused by the probe and the flaw and note several ways to minimize this effect. One method that is currently in use at Carleton

is the spot welding of the measurement probe pairs, in the form of small lengths of wire onto the specimen surface. They show that the induction effect is reduced when the height of the probe pairs is as low as possible above the surface. The phase sensitive detector incorporated in the Micro-Gauge also helps to reject the induced signal. This is because the induced signal and the measurement signal are shifted out of phase by $\pi/4$. The problem of measuring surface cracks that are not long in relation to the plate width was addressed by Michael et al {93}. They obtained good results for the single crack case but did not discuss the physically important case of multiple crack arrays.

Leading Canadian and British researchers met in 1986 at Memorial University in Newfoundland. They agreed on a set of suggestions and guidelines for the improvement of the accuracy and repeatability of ACPD measurements, Connolly et al {94}. The present program was conducted in conformity with these guidelines.

3.2 AC Field Studies of Rectangular Bars

One of the primary assumptions that has been made in the adoption of the ACPD crack measurement system is that the electrical field induced in the specimen is uniform as shown in Figure 3.2. Many experimenters have therefore used only one or two reference potential drop values for use in the crack depth calculation developed by Dover et al {92} and Dover {95}.

$$a = \frac{\delta}{2} \left(\frac{V}{V_{REF}} - 1 \right) \quad (3.2)$$

where a is the crack depth,

δ is the probe spacing,

V is the potential drop of the active probe-pair,

and V_{REF} is the reference probe-pair potential drop.

The uniform field assumption implies that the voltage drop between the probes of a given probe-pair is a function only of the distance between the probes making up that pair. To investigate this assumption, the electrical field induced by an AC current in rectangular bars was examined. The potential drop distribution was measured by means of an array of wire probes spot welded to the surface of bars of steel. The resulting measurements were plotted and the distribution of the field was deduced from these plots. Different configurations of the field input probe array and specimen geometry were assessed to determine if there is an optimum configuration for electrical crack measurement work.

It has been found that the electrical field induced in rectangular specimens is not uniform. An improved method of wiring the ACPD probe array has been developed that addresses this problem. A simple correction has been developed that significantly improves the accuracy of the crack depth measurements made during the fatigue test before any optical calibration of the beachmarks is available. These enhancements have resulted in potential drop measurements that are extremely stable and very repeatable.

3.2.1 Description of Test Specimens and Experimental Equipment

The specimens employed in this study were milled to size (Table 3.1) from the

same type of high strength-low alloy (HSLA) steel used in the fatigue tests described in Chapter 4. The initial study of the influence of the field input probe configuration was carried out on specimen No. 2, Figure 3.3. An array of 60 potential drop probes (30 probe-pairs) was spot welded to the top surface of the bar. Due to symmetry, only one half (an area 150 mm X 325 mm) of the specimen was instrumented with potential drop probes. The probe-pairs consisted of short (2 mm) copper clad steel wires welded to the top surface of the bar. The wires forming a probe-pair were spaced 10 mm apart and were positioned with perspex jigs prior to welding. The probe numbers referred to in the figure are the same as those shown in Figures 3.4-3.8.

Specimens 1-4 were used to study the effect of specimen geometry and field probe location. Each specimen had two arrays of potential drop probe-pairs spot welded onto three surfaces. The first array was mounted perpendicular to the longitudinal axis at a position mid-way between the end and the centre of the bar, i.e. at a position corresponding to 25% of the length of the bar. The second array was mounted parallel to the first at the centre of the bar as shown in Table 3.1. The PD were made of shielded multi-conductor cable. The leads were each connected to a multi-channel scanner that sequentially scanned through the channels on command from a PC based data acquisition system. The output of the scanner was connected to the Micro-Gauge and then through an A/D converter to the computer for storage on diskettes.

3.2.2 Field Measurements

The potential drop distributions for various field input probe configurations are

shown in Figures 3.4-3.8. These are 3-D surface plots showing the magnitude of the potential drop at a given probe-pair as a vertical displacement above a representation of the 150 mm wide by 325 mm long instrumented surface of the specimen. The potential drop data has been normalized by the largest value in the data set for that field probe configuration. It can be seen that once a distance of about one specimen-width from the input end of the specimen had been traversed, the field assumes approximately the same shape for each of the input probe configurations. It can also be noted that for each of the field input probe arrays, the field at the centre line of the specimen is not uniform.

The normalized potential drop distributions for various specimen geometries are shown in Figures 3.9-3.12. The independent variable is the position of the probe-pair relative to a zero line set at the left hand edge of the specimen. The surface of the specimen has been unfolded so that the middle area of each figure represents the top surface of the bar. The left and right hand areas represent the left and right hand sides of the specimen respectively. The shape of the field at the centre line PD probe array indicates that in each case the current density is higher at the edges of the specimen than along the centre line. This effect is more evident when the specimen width to thickness ratio approaches one, ie. the specimen becomes more square in cross section, as in specimen No. 4.

The current density on the sides of the specimen is higher near the lower surface than it is near the upper surface. This is due to the presence of the field input wires, which have a strong influence on the field induced on the lower surface of the specimen, running directly down the underside and meeting beneath the centre. It is necessary to

have the field input wires situated this way to avoid an even greater inductance effect on the top surface. In any case the field on the sides of fatigue specimens is not relevant to the process of crack measurement. It was found that the shape of the field was not greatly affected by whether the field input probes were located on the top surface or on the end of the specimen.

3.2.3 Description of the Improved ACPD Probe Array

An ACPD array consists of active probe-pairs that span the crack and reference probe-pairs that do not span the crack. The crack depth, a , is then calculated from the ratio of the potential drop of the two probe-pairs according to equation 3.2. The fact that the location of the probe-pair affects the potential drop implies that a more accurate measure of the depth of a crack between an active probe-pair can be made if the reference probes and associated active probes are in close proximity to each other.

The improved ACPD probe array provides for each measurement position across the specimen to have three probes spot welded in place as in Figure 3.13. Two of the probes are welded on the same side of the crack while the third is on the other side. The first two probes make up the reference probe-pair while the middle and the third probes make up the active probe-pair.

3.2.4 Discussion

The experimental study into the distribution of electrical potential on the surface of rectangular bars of steel has shown the following:

1. It has been found that the location and number of the field input probe(s) does not greatly influence the shape of the electrical field assuming that the field measurements are made at least one specimen-width away from the point of field input.
2. The strength of the field is not uniform across the specimen, even in a plane taken at right angles to the specimen's longitudinal axis and mid-way between the points of field input. This is the location where crack measurements would most likely be made.

3.3 Corrected Potential Drop Data

From equation 3.2 it can be seen that if the crack depth 'a' is zero, as it would be at the start of the fatigue test, then the ratio $\frac{V}{V_{REF}}$ should equal one. This has rarely been found to be the case. The initial potential drop ratio is usually in the range 0.9 to 1.1 and if equation 3.2 were applied directly to the data, it would indicate that a crack of up to 10% of the probe spacing was present even though the specimen was initially uncracked. The reason for this discrepancy in the initial PD ratio is likely that the contact resistance of the active and reference probes is slightly different or that the presence of the notch has somehow affected the field. Since these effects are present throughout the test, the PD data used in the calibration polynomials for crack depth calculations is modified or corrected by the initial PD ratio as shown below in equation 3.3.

$$\left(\frac{V}{V_{REF}}\right)_{CORRECTED} = \frac{V}{V_{REF}} \left(\frac{V_{REF}}{V}\right)_{INITIAL} \quad (3.3)$$

where $\frac{V}{V_{REF}}$ and $\left(\frac{V}{V_{REF}}\right)_{INITIAL}$ are the current and the initial PD ratio respectively. Thus

$$\text{the crack depth is then } a_{CORRECTED} = \frac{\delta}{2} \left[\frac{V}{V_{REF}} \left(\frac{V_{REF}}{V}\right)_{INITIAL} - 1 \right]$$

One of the fatigue specimens of the present program was used to check the validity of this correction. The specimen was instrumented with probes as described in Chapter 4 and was tested to failure. The beachmark depths were compared with crack depths calculated using the corrected data from the three probe sets (as used in equation 3.3) and also using two reference probes (equation 3.2) located 33% and 66% across the width of the specimen in the manner usually employed. Figure 3.14 shows the results of this comparison. Note the much better accuracy of the corrected calculation compared to the two-reference method. This correction is very useful for determining the crack depth when the calibration curves obtained after the test are not available, as is the case during the fatigue test.

3.4 Calibration of the ACPD Method

One of the chief difficulties with potential drop electrical crack measurement methods is that of calibration. The relationship between the crack length 'a' and the probe spacing 'δ' is not usually linear, and as was outlined in Section 3.2, is also dependent upon the location of the probe-pair on the surface of the specimen. For this reason, several different approaches have been used to calibrate ACPD equipment. The Unit Inspection Company recommends the 1-D solution as shown in Figure 3.1. They also give some correction curves for cases where the crack is either semi-circular or semi-elliptical in shape, Figure 3.15.

It should be noted that these curves have been derived for the case of a single crack in an infinite plate. McFadyen {29} has found that the curves give reasonable

accuracy when there is indeed only one crack in a wide plate. It has been found to give poor accuracy in cases where there are multiple cracks, or when the cracks have a high aspect ratio (depth/width). The use of the curves also requires that the user make measurements of the surface length of the crack. It is not always possible to make this measurement with a reasonable level of confidence. Bain {28} at Carleton University noted the inaccuracy of the 2-D curves for determining crack depths for elliptical cracks. Equation 3.2 had to be modified for use in his study of weld undercuts. He used the calibration procedure outlined below and obtained good results. The inadequacies found by Bain may be due to the fact that the 2-D curves were derived from flat plate specimens while he was studying T-plate specimens.

The other approach to the calibration of ACPD work to date has relied on experimentally obtained calibration curves of potential drop versus known crack depth. The known cracks were made by EDM notches in test specimens, {29}, or by producing beachmarks and taking a PD measurement or both. After the test was completed, the specimen was broken apart and the depth of the beachmark was compared to the depth measured electrically. This is the approach used in the present study.

The beachmarks left on the fracture surface after the fatigue test were used with the corresponding ACPD data to obtain polynomials relating crack depth 'a' with the potential drop adjusted for specimen geometry effects as noted above in Section 3.3. An example of the fitting of ACPD data to beachmarks depths is shown in Figure 3.16. The figure shows the optical beachmark measurements as points and the polynomial relating potential drop and crack depth as a line. Note that the fit is not simply a linear

interpolation between data points but is a spline polynomial that is constrained to pass through all of the data points. A polynomial fit such as shown in Figures 3.16 (a) and (b) was performed for every active PD channel on every specimen in the present study. This task was accomplished using data handling software to assemble the data files and a curve fitting package for determining the coefficients of the polynomials. These coefficients were then assembled into a look-up table that was used by the data post-processing program to determine crack depths. This post-processing software will be described in Chapter 4. An example of the accuracy obtained by this method of fitting beachmark and ACPD data is shown in Figure 3.17. This figure shows a representation of the fracture surface of specimen 02A. The beachmarks that were put on the specimen during the fatigue test are shown as solid lines while the calculated crack depths are shown as points.

It should be noted that the calibration procedure described above is used only after the fatigue test has been completed and the fracture surface is available for visual inspection. During the test, the correction procedure outlined in section 3.3 and the simple 1-D expression (equation 3.2) is used to obtain an approximation of the current crack depth and shape.

Specimen No.	W (dimensions in mm)	T (dimensions in mm)	L (dimensions in mm)
1	150	25	650
2	150	50	650
3	100	50	650
4	50	50	650

Table 3.1 Field Study Test Specimen Characteristics

$$\Delta V_{REF} = f(\delta)$$

$$\Delta V = f(\delta + 2a)$$

$$\frac{\Delta V_{REF}}{f(\delta)} = \frac{\Delta V}{f(\delta + 2a)}$$

$$a = \frac{\delta}{2} \left(\frac{V}{V_{REF}} - 1 \right)$$

Figure 3.1 The ACPD Method - I uncracked body

II cracked body

Figure 3.2 The Uniform Field Assumed to Exist in Rectangular Bars Conducting an Alternating Current. (Ref. 89)

probe numbers are shown below
the probe positions

Figure 3.3 Field Input Probe Configuration - End View of Specimen No. 2.

Figure 3.4 Field Distribution with One Input Probe (at Position 4).

Figure 3.5 Field Distribution with Two Input Probes (at positions 2 and 6).

Figure 3.6 Field Distribution with Three Input Probes (at positions 1, 4, and 7).

Figure 3.7 Field Distribution with Five Input Probes (at positions 1, 3, 4, 5, and 7).

Figure 3.8 Field Distribution with All Seven Input Probes Connected.

Figure 3.9 Field Distribution of Specimen No.1.

Figure 3.10 Field Distribution of Specimen No. 2.

Figure 3.11 Field Distribution of Specimen No. 3.

Figure 3.12 Field Distribution of Specimen No. 4.

Figure 3.13 The Three Probe Set ACPD Probe Array.

Figure 3.14 ACPD Results Comparison of 3-Probe and 2 Reference Method for Specimen 02A.

Figure 3.15 The 2-D Correction for Semi-Circular and Elliptical Cracks. (ref. 101)

Note - a_i = crack depth indicated by the instrument
 a = actual crack depth

Figure 3.16 Examples of the Fitting of Beachmark Data and Potential Drop Data to Obtain a Polynomial Relating the two.

Figure 3.17 Comparison of Beachmarks and Calculated Crack Depths for Specimen 02A.

Chapter 4 Experimental Program

4.0 Introduction

This chapter describes the fatigue test program, the fatigue rig, the data acquisition equipment, and the test specimens. The preparation of the specimens is described in some detail as it is a fairly involved process that utilizes some novel techniques.

4.1 The Fatigue Test Program and Test Procedures

The test program was carried out using existing test equipment which includes a computer controlled MTS closed loop hydraulic fatigue testing rig, a Unit Inspection Crack Micro-Gauge, and an IBM PC-XT based data acquisition system, Figure 4.1. The test program consisted of fatigue tests on 24 steel specimens. This section gives a general overview of the conduct of a typical fatigue test. The specimens were cyclically loaded

in four point bending and were tested at a constant stress amplitude. The stress levels were calculated as the nominal bending stress at the root of the machined notch on the top surface of each specimen. All of the tests were run at a stress ratio, $\frac{\sigma_{MIN}}{\sigma_{MAX}} = 0.05$. Some of the specimens were tested without any crack initiators or starters, while others had one or two starters placed at the root of the notch to simulate an existing array of cracks.

The growth of the fatigue cracks was monitored by three different techniques. The crack fronts were marked with an ink stain once they had reached a measurable size, usually about 1-2 mm. The ink stain served two useful functions. It provided an indication of the number of natural initiation sites and it acted as the first beachmark. This ink stain was produced by the following procedure;

- i) hold the crack open by applying 75% of maximum load,
- ii) apply machinists blue dye to the crack region with an eye-dropper,
- iii) allow the dye to dry overnight.

The testing was continued until the deepest point on the crack front had grown to about a 2-4 mm depth. The load amplitude was then halved and the frequency doubled to produce a beachmark. The beachmark cycling continued for from 25 to 150 keycycles, depending on stress level, or until a measurable difference in ACPD readings was obtained indicating that the crack had grown by a small amount.

This beachmarking procedure was repeated periodically as the crack grew until the specimen failed. Between three and six beachmarks were produced on each specimen.

At this point the specimens were removed from the rig and broken apart. Small coupons were cut from the crack face for ease of handling. The beachmarks and corresponding ACPD measurements were compared to obtain a calibration equation for the specimen. The calibration procedure was described previously in section 3.4.

4.2 The Test Specimen

4.2.1 Specimen Material

The test specimens were machined from a plate of Canadian-made high strength low alloy (HSLA) structural steel. The plate was acquired from the Materials Technology Laboratory (MTL) especially for the purpose of conducting fatigue tests. This steel is a candidate for certification for offshore use and has been manufactured to the Lloyds L160 specification in order that the Canadian tests can be compared to tests done in Europe on other L160 steels. Table 4.1 gives the chemical composition and mechanical properties of the steel.

4.2.2 Specimen Geometry

The difficulty with studying crack coalescence in actual welds is that, typically between 3 and 5 crack initiation sites per inch of width occur in fatigue cycling. Since it was desired to study the cusp region between adjacent cracks, it was felt that a study of this large number of cracks would not be manageable. It would be impossible to ensure that the crack measuring equipment was situated correctly relative to any pair of cracks.

For this reason it was decided to simulate the weld geometry and to provide only two crack initiation sites. The specimen geometry has been designed to simulate the 135 degree notch that is formed at the foot of 45 degree fillet welds. Previous work has indicated that un-improved weld toes have a radius of approximately 0.2 mm. The length and width of the specimen are the largest that could be fitted into the available testing machine.

The geometry of the test specimen and details of the notch are shown in Figures 4.2 and 4.3. Each specimen was stamped with an identification mark that would be present on the coupon after the test was completed and the specimen broken apart.

4.2.3 Loading Fixture

A fixture (shown in Figure 4.4) was designed and built to load the specimen in four point bending. The centre to centre distance of the 50 mm diameter roller pins was 150 mm. The fixture was equipped with large roller bearings that allowed the 50 mm diameter pins to turn slightly as the specimen deflected. This lessened the tendency of the pins to fret on the underside of the specimen during the test.

4.2.4 Specimen Preparation

After the 135 degree vee-notch was machined in the top surface of the specimen, the ends of the notch were carefully rounded with a fine file and then polished with emery and crocus cloth to remove scratches. This was done so as to reduce the stress concentration at the end of the notch and lessen the effect of the notch end on crack

difficult process. The probes were made of short lengths of copper clad steel wire welded in place by means of a capacitive discharge spot welding machine that was built in-house. The spot welder operates at between 250 and 300 volts and provides a single pulse of current sufficient to butt weld one probe onto the surface of the specimen.

To ensure accurate spot welding of the ACPD probes in the notch, a simple plexiglass jig was used. The jig was machined to fit into the notch and a pattern of holes drilled in the jig provided a guide for the probe wires while they were being welded into place. The use of such a jig is important in order that the probe wires be accurately placed and securely welded. The breakage of probe wires during a test was a major problem in the early part of the test program.

The specimens without a starter crack had an array of 16 three probe sets (as described in Chapter 3) installed at intervals of 10 mm across the width of the specimen. The later specimens that had starter cracks were equipped with a modified probe array. This arrangement concentrated the greatest number of probes in the region between the two starters where crack coalescence would take place. The spacing of the probe sets in this region was 5 mm. While it would be desirable to have them even closer together to provide more data in the region of interest, this was the closest spacing possible with the size of lead wires used. Once the probe array was installed, the welding jig was removed and the probe wires clipped to a length of 2-3 mm. The lead wires in the harness were then prepared and soldered onto the probes as shown in Figure 4.5.

2 of/de 2

MICROCOPY RESOLUTION TEST CHART
NATIONAL BUREAU OF STANDARDS
STANDARD REFERENCE MATERIAL 1010a
(ANSI and ISO TEST CHART No. 2)

growth. It also acted to inhibit crack initiation at the edges. The starter cracks were machined into the root of the notch at the MTL facility. This was done by means of the electrode discharge machining (EDM) process. Previous researchers have used other methods such as slitting saws for placing starters, but it was felt that the EDM method produced a more natural crack-like shape for the initiator. The EDM process uses an expendable electrode and a high electrical current to erode the work piece. The shape of the starter formed in the specimen was determined by the initial shape of the electrode.

The electrodes were made of small pieces of brass plate soldered to a brass tube which was fitted into a fixture on the EDM machine. The specimen was immersed in a tank that was filled with an insulating oil. The tube supporting the electrode also carried a stream of insulating oil into the erosion region of the specimen surface to carry away the metal cuttings produced by the erosion process. The machining of a specimen took approximately 15-24 hours depending on the depth and shape of the electrode used. The actual shape and depth of cut was somewhat difficult to predict because both the work piece (specimen) and the electrode are consumed during the EDM process.

Two strain gauges were installed on the underside of the specimen, one near each edge. This was done so that any twisting of the specimen in the loading fixture could be detected and the support beams adjusted to compensate prior to the beginning of the test.

4.2.5 The ACPD Probe Array

The next step in the preparation of the test specimens was the installation of the ACPD probe array. This operation took some time to perfect and was, even then, a

4.3 The Data Acquisition System

The data acquisition system used in this project consists of four major pieces of equipment. These are;

- i. Unit Inspection Crack MicroGauge
- ii. Keithley 706 Scanner
- iii. MDTest Control and Data Acquisition Card
- iv. IBM-PC/XT computer

The purpose and operation of each of these components will be briefly described below.

The Unit Inspection Crack Microgauge is a highly specialized piece of equipment used for measuring the length of cracks in metallic materials. It has two separate systems: a field input system and a potential drop measurement system. The field input system outputs a carefully regulated AC signal at a constant current level and frequency. The level of the output current is approximately 1.0 Amp and the frequency of the signal is 6 kHz.

This signal is carried by shielded wires to probes that have been welded onto the test specimen. As explained in Chapter 3, the conduction of an alternating current through a metallic body results in the current being conducted only in a thin skin on the surface of the body. For the type of specimen material used in this project and the frequency of the Micro-Gauge the skin thickness is approximately 0.1 mm. The potential drop measurement system is essentially an extremely sensitive voltmeter. It incorporates a Wein Bridge circuit that enables the instrument to measure voltage differences on the

order of a few microvolts. The measurement circuit is also equipped with filters that make it phase-sensitive only to the input current.

It has been found that the best results are obtained if the average Micro-Gauge reading at the beginning of testing is about 200. Some channels will be somewhat higher and some will be lower but this is a good value to use as a guide. The choice of initial value depends on the thickness of the specimen since the Micro-Gauge has a fixed maximum output of 2000 microvolts and thus the maximum measurable crack depth is fixed once the probe spacing is known.

The Keithley 706 Scanner is a device that sequentially connects the ACPD channels to an input which is connected to the Crack Micro-Gauge. The scanner switches channels either at a regular time interval as set by the operator or by a trigger signal sent by some other component of the data acquisition system. The MDTest system (described below) sends such a signal in the present system.

The connection from the ACPD probes to the Crack Micro-Gauge is a procedure that requires organization and care. The potential drop signals are so tiny that any poor connections will ruin the accuracy of the entire system. The cables used in the system are made of Belden type 9456 four conductor cable with integral foil and bare wire shield. The probe array is connected to a short harness which is then connected to the rest of the measurement system by means of a high quality 50 pin connector. The integrity of the cable shielding is very carefully maintained throughout the entire crack measurement system. It has been found that the system is less affected by extraneous signals than the previous unshielded system. Repeatability and stability of the ACPD

readings even at high Micro-Gauge gains is excellent. Even with the shielding, it has been found necessary to keep the field and PD cables as widely separated as possible to eliminate interference on the PD channels. Once a test has begun, the cables were not moved around. It has been found that this can affect the measurements and give poor results.

The channel numbering system runs from the right hand side of the specimen when viewed from the front of the rig. Odd numbered channels are reference channels and even numbered channels span the crack region and are therefore active. The connections in the scanner are made on removable cards that each handle up to ten channels.

The MDTest controller and data acquisition card is mounted in an IBM-PC/XT computer. This board incorporates a signal generator and fault diagnosis circuitry that controls the MTS servo-hydraulic fatigue tester and the data acquisition system. The user is able to specify the waveform and amplitude of the load to be applied. A random load sequence can be read from a separate disk file and implemented if desired.

The MDTest system was programmed to record data for a given channel until five readings that varied by less than one percent were obtained. This usually took approximately eight seconds per channel or about 4 minutes for a 32 channel set.

4.4 Data Post-Processing

After a fatigue test was completed the specimen was cut in half with a bandsaw. This cut was made from the underside of the specimen across the lateral centre-line in

such a way as to not damage the fracture surface. A coupon, approximately 10 mm thick, was then cut from each half. The coupon was examined and the depths of the inkstain and the various beachmarks at each probe pair location was noted.

These depth data were then combined with the appropriate ACPD data as outlined in Section 3.4 and a set of polynomial coefficients was produced for each channel relating crack depth and potential drop. This set of coefficients was then used by a data reduction program known as ANSWERS that produced the following outputs:

- i) crack depth
- ii) crack growth rate (da/dN)
- iii) stress intensity factor.

The ANSWERS program produced a file of all crack depths at all probe-pair locations and another file containing crack depth, growth rate and stress intensity factor information at particular chosen probe-pairs. The calculation of the crack growth rate was done according to the seven point polynomial method described in ASTM E-649 {96} and the stress intensity factor solution is due to Bell et al {21}, {26}, {27} and Bell and Kirkhope {22-25}. The contents of this second file are chosen according to what type of specimen is being analyzed. There are three types of specimens as described in Chapter 5.

<u>Element</u>	<u>Composition</u>
C	0.12
Mn	1.39
Si	0.28
S	0.11
P	0.017
Al	0.027
Cr	0.15
Mo	0.17
Cu	0.01
V	0.053
Nb	0.041
Yield Strength	390 MPa
Ultimate Strength	517 MPa
Elongation	32 %

Table 4.1 Properties of the Test Specimen Steel

Figure 4.1 Test Rig Including Loading Frame and Data Acquisition Equipment.

Figure 4.2 Test Specimen General Arrangement.

$L = 588 \text{ mm}$

$W = 160 \text{ mm}$

$T = 52 \text{ mm or } 32 \text{ mm (see Chapter 5)}$

Thickness at the notch = $T - 13.0 \text{ mm}$

See Figure 4.3 for notch details.

Figure 4.3 Section Showing the 135° 'Vee' Notch and the Root Radius of the Specimen. Dimensions in mm. The first two specimens had a 5.0 mm and a 1.0 mm root radius respectively while all of the remaining specimens had 0.2 mm radius.

Figure 4.4 Specimen Loading Frame.

Note that the wires are laid
as close as possible to the
surface of the specimen.

Figure 4.5 Probe Leads
Soldered onto the
Probes.

Chapter 5 Results and Discussion

5.0 Introduction

This chapter will describe the three groups of specimens tested and it will present typical results of the fatigue testing for several specimens. There will also be a description of the crack shape development models used in the data reduction and life prediction computer programs and then a discussion of the results of the test program.

The experimental program consisted of fatigue tests run on 24 four-point bending specimens as was described in Chapter 4. All of the testing was done using constant amplitude sinusoidal loading at a stress ratio ($\sigma_{\text{MIN}}/\sigma_{\text{MAX}}$) of 0.05, this being as close to zero as was possible without unloading the test rig support beams during load cycling.

Three types of specimens were tested;

Type I specimens which had no, or only a single starter crack,

Type II specimens which had two starter cracks at the notch root,

Type III specimens which had a single weld pass placed at the notch root as shown in Figure 5.1.

Note that there is no specimen 020 due to the obvious possibility of confusion in the name of the specimen.

The original plan for the experimental program called for the testing of type I specimens without starters. However, after a total of seven specimens had been tested, it was determined that the cracks were forming and coalescing at such a shallow depth that it was impossible to study the coalescence process. This led to the testing of the type II specimens wherein the initiation and growth of two dominant cracks were made certain by the presence of the starters. In later type II specimens the region between the two starters was fitted with an ACPD probe array of much closer spacing (5 mm rather than 10 mm). The increased density of probes allows much better detection and monitoring of the crack coalescence process than would be obtainable with the wider spaced probe array.

The type III specimens were tested to study the effect of the presence of a weld in a notch region on the crack initiation and coalescence processes. A stress-life summary of specimens tested is shown in Figure 5.2.

The crack growth rate (da/dN) was calculated by numerically differentiating the crack depth versus cycle count data provided by the ACPD measurements. This was performed according to the procedure outlined in ASTM E649 {96} which requires fitting a least squares parabola to successive sets of seven data points. In this way, numerical

stability is obtained while significant changes in the crack growth rate are not "washed out" in the analysis.

The stress intensity factors shown were calculated based on the finite element analysis done by Bell et al {21-27} as described in Chapter 2 of this thesis. Both two dimensional and three dimensional stress intensity factors were produced. The change from 3-D to 2-D was made at the point where the aspect ratio of the crack was calculated to be less than 0.2.

5.1 Type I Specimen Testing

The first two type I specimens were machined with a 5.0 mm and a 1.0 mm notch root radius respectively. All of the remaining seven type I specimens as well as all of the types II and III specimens had a 0.2 mm radius machined at the root of the notch. Table 5.1 shows the test conditions and results obtained from the type I specimens tested.

Specimen 5A (the first specimen in the program) was originally tested at a stress amplitude of 150 MPa for over 2×10^6 cycles with no apparent crack growth. The stress range was then increased to 175 MPa for a further 1.5×10^6 cycles and then to 240 MPa which corresponds to the maximum rated load for the fatigue rig. The specimen failed after a further 904 kcycles but the results from this test are not included in any further analysis as they were not obtained at a constant stress level as were all of the others except specimen 02W which is discussed below.

The failure can be traced to two points, one close to an edge and the other near the middle of the specimen. The middle flaw grew to a depth of about 8.5 mm and an

aspect ratio of 0.5 before it coalesced with the side flaw. At this point the side flaw had reached a maximum depth of 23 mm but had long since broken through the side of the specimen and so was not a truly semi-elliptical crack. After coalescence the crack reached the other edge of the specimen but at a fairly shallow depth and then general yielding occurred.

Specimen 1A was tested at a stress amplitude of 240 MPa but post-test examination of the fracture surface showed that the cracks initiated, coalesced and grew as a straight fronted crack at a depth of less than 0.5 mm. This was also the case with specimens 02A and 02G. The shallow coalescence depth limits the amount of crack shape development data obtainable from these specimens. However, the crack growth rate versus stress intensity factor range data for the two dimensional straight fronted crack is useful for evaluating the Paris law growth constants C and m which were first introduced in equation 2.7 of this thesis. Figure 5.3 is a graph of crack growth rate versus stress intensity factor range for specimen 1A. Since the graph is plotted using logarithmic scales the slope of the line through the data is the quantity m while the vertical axis intercept is the quantity C .

The fracture surfaces of specimens 02B and 02C are remarkably similar in appearance. In both cases, the crack initiated at a single point about 10% of the width from the specimen edge and then grew in the classical semi-elliptical shape development pattern. Unfortunately due to its location the crack soon broke through the edge of the specimen and then grew in an asymmetric manner until failure. The evolution of the crack is illustrated in Figure 5.4 which is a photograph of the fracture surface of 02C.

The fracture surface of specimen 02E reveals semi-elliptical crack growth not only from the single starter in the middle of the specimen but from sites initiated near each edge of the specimen. Coalescence of these three cracks began when the centre crack was 5 mm deep and 25 mm wide, ie. an aspect ratio of approximately 0.40 and the side cracks were each about 10 mm deep. The side cracks broke through the edges long before coalescence with the centre crack occurred and were not therefore truly semi-elliptical. The initiation of side or edge cracks was much less of a problem in specimens subsequent to 02E because a larger radius was ground and polished into the edges of the specimens before the test was begun. Figure 5.5 is a typical graph of crack depth versus number of cycles for a type I specimen, in this case 02E.

Specimen 02D was tested at a stress amplitude of 150 MPa for over 9×10^6 cycles with no evidence of a fatigue crack. Specimen 02S was tested at a stress amplitude of 120 MPa for over 15×10^6 cycles also with no evidence of fatigue cracking. One very interesting fact noted during the testing of this specimen (and that of specimen 02D) was the excellent stability of the Crack Microgauge readings. For specimen 02S, the fatigue rig was operated continuously for over 1500 hours (about 9 weeks) and ACPD data was collected approximately every 4 hours. The maximum variation in potential drop values was less than 1% between the start and finish of the test. These two specimens are shown as run-out data points on Figure 5.2.

5.2 Type II Specimen Testing

A total of eleven type II specimens were tested with various double starter crack

configurations and at various stress amplitude levels. Two different specimen thicknesses were employed, 39 mm and 19 mm. The test conditions and starter configurations of the type II specimens are detailed in Table 5.2. Also in this table are the dimensions of the single crack that resulted from the coalescence of the two starters and the depth that this crack attained at the point where it broke through the edges of the specimen. These quantities are used in the crack shape development model that will be discussed in a subsequent section of this chapter.

Specimen 02F was the first of the double starter crack specimens to be tested in the present program. Figure 5.6 shows the crack depth versus cycle count data for the probe-pairs at the centre of each of the two starters (denoted LEFT and RIGHT on the figure) and at the centre position (MIDDLE on the figure) between the starters. It can be seen from the figure and by examination of the fracture surface, that there was no crack growth at the central position for a significant number of cycles after the start of the test and that coalescence was not complete for a substantial number of cycles after central position crack growth began. The portion of the fatigue life between central position crack initiation and the completion of coalescence is defined as the coalescence period of the specimen. It is useful to note the percentage of the total life of the specimen that is consumed in the central position initiation and coalescence periods. The reason for this is that existing design codes assume that coalescence is instantaneous once the two adjacent cracks begin to interact. Thus, the codes assume that there is no coalescence period at all.

Table 5.3 lists the central position initiation and coalescence periods as well as the

total life for all of the type II specimens. In the case of specimen 02F both the initiation period and the coalescence period were 100 kcycles. Thus each accounted for 43% of the total life of 230 kcycles.

The probe array of specimen 02H was installed such that there was no probe-pair located at the left hand starter position. This is rather unfortunate as it limits the comparison of growth between the left and right starters.

Specimen 02J was tested with fairly deep, wide starters placed quite close together (only 15 mm apart). This resulted in a short (10 kcycle) initiation period. This represents less than 2% of the 564 kcycles total life. The crack coalescence period of this specimen consumed a further 200 kcycles or 36% of the total life leaving a substantial portion (62%) of life for post-coalescence crack growth. The crack depth versus cycle count data for this specimen are shown in Figure 5.7.

The amount of data available from specimen 02K is very limited due to a data acquisition computer problem. This is very unfortunate, as examination of the fracture surface reveals very good elliptical crack growth and a well defined coalescence region or cusp (the triangular shaped area between the semi-elliptical starters).

The coalescence of the relatively shallow, wide starters of specimen 02L into a single crack consumed a large portion of the total life. The initiation period was only 12 kcycles (8.3% of the total) and the coalescence period was 97 kcycles out of a total life of 145 kcycles, this being nearly 67% of the total.

Conversely, specimen 02M was tested with starters that were quite deep and set close together. This accounts for the fact the initiation and coalescence periods for this

specimen consumed only 44 keycycles or 34% of the total life of 130 keycycles. The crack depth versus cycle count curves for this specimen are shown in Figure 5.8.

The underside of the last three type II specimens 02P, 02Q and 02R were milled to reduce the thickness at the notch root to 19 mm. It is clear from Table 5.3 that both the initiation and the total life are increased over similar 39 mm thick specimens tested at the same stress amplitudes. The initiation period of specimen 02P at the central position was 200 keycycles or about 46% of the total 435 keycycle life. The coalescence period consumed 130 keycycles or 30% of the total life of the specimen. Figure 5.9 is a photograph of the fracture surface of specimen 02P.

Specimen 02R was the final type II specimen in the present program. The very long life of this specimen (over 2×10^6 cycles) was obviously due to its relatively low stress level and to the fact that it is a thin (19 mm rather than 39 mm) specimen. It can be seen from Figure 5.10 that the growth of the starters is uneven after about 1000 keycycles or 44% of the 2268 keycycle total life. The initiation at the central position occurred at approximately 200 keycycles (just under 9% of the total). The uneven crack growth makes the definition of the crack coalescence period somewhat more difficult but a reasonable estimate would be that coalescence occurred at 1750 keycycles when the central crack depth equalled that of the right hand starter. This represents a coalescence period of 750 keycycles or about 33% of the total. Subsequent to this, the central position grew to a depth that was between the right and left hand positions, the difference between these gradually increasing until failure occurred.

5.3 Type III Specimen Testing

The test conditions and major results for the four type III specimens are summarized in Table 5.4. It can be seen from the Table that, except for specimen 02W which failed on two crack planes, the total lives obtained for these specimens were substantially shorter than those obtained for non-welded specimens tested at similar stress amplitudes and similar thicknesses. This is due to the fact that the cracks initiated at a large number of separate sites and coalesced to grow as a single straight fronted crack. This coalescence took place at a very small crack depth. For this reason, the amount of information available from these specimens on the coalescence and crack shape development processes was very limited.

The fracture surface of 02T shows that the failure initiated at more than 40 sites across the width of the specimen and that these flaws coalesced into a single straight fronted crack and broke through the specimen edge at a depth of less than two mm.

Specimen 02U was a 19 mm thick specimen tested at the same 170 MPa stress level as the 39 mm thick specimen 02T. The total life of this specimen was found to be much longer than the thicker specimen. This was due to the well documented thickness effect that has been noted in the literature {13}, {14} and {20}. Examination of the fracture surface showed that the crack initiated, coalesced and grew to the specimen edges in a very similar manner to that found in specimen 02T. The total life of specimen 02V was found to be shorter than that of the other 19 mm type III specimens 02U and 02W due to the higher stress range of the test. As with specimens 02T and 02U, the failure initiated at many sites and the flaws quickly coalesced into a single crack with a very low

aspect ratio (< 0.05) before a depth of two mm had been reached. This single crack grew to a depth of approximately seven mm before breaking through the edges of the specimen.

The final specimen in the present program, 02W was tested initially at a stress range of 150 MPa, but after 3.35×10^6 cycles, no evidence of a crack was found and so the stress level was increased to 200 MPa. Testing continued for over 2×10^6 cycles before the specimen failed. The failure was quite different from all of the others in the present program as it occurred on two different planes. In fact, part of the fatigue crack was between the reference probes rather than between the active probes. Due to this fact and because the stress level was changed during the test, the results from this specimen are not included in the rest of the analysis.

5.4 Crack Shape Development Models

5.4.1 Type I and Type III Crack Shape Development

The crack growth of the type I and III specimens was found to be strongly dominated by straight fronted crack growth following a relatively short period of semi-elliptical crack growth and crack coalescence. This is similar to the failure of welded tee plate specimens tested by Vosikovsky {13} et al and so the crack shape development model developed by him for welded geometries was used. In order to determine the value of the exponent k in the Vosikovsky model (equation 2.11) it is necessary to plot a graph of the natural log of the aspect ratio of a semi-elliptical crack versus the crack depth, such as Figure 5.11 which shows the data for specimen 5A. The slope of the line through these data points is the value of k . Only specimens 5A, 02B, 02C and 02E

exhibited sufficient semi-elliptical crack growth for it to be possible to obtain aspect ratio versus crack depth data.

Examination of the remaining type I and all of the type III specimens in the present study showed that the cracks coalesced into a single essentially straight fronted crack at such a shallow depth that no truly semi-elliptical crack growth could be measured. Therefore, a value of the coefficient "k" in equation 2.11 of 0.85 was chosen since, as can be seen in Figure 5.12, it causes the aspect ratio of the crack to drop to a value of 0.2 at a depth of less than two mm. The curve in the figure is not meant to represent the data from any particular specimen, it merely shows the form of the crack shape development model used in the data reduction and life prediction programs. The values of k found for the other specimens are given in Table 5.5.

5.4.2 Type II Crack Shape Development Models

The crack shape development model described in equation 2.11 assumes implicitly that the initial shape of a flaw is semi-circular ie. the aspect ratio is 1.0. This can be seen from the fact that as crack depth approaches 0, the value of a/c approaches 1.0. This is however not true for type II specimens as the initial flaw shape is known for all of the specimens and it is not close to semi-circular for any of them.

Therefore a crack shape development model has been developed for the type II specimens in the present study. It is based on the fact that the flaws grow from a known shape at a known crack depth (the initial starter shape and depth) to another known shape and depth just before crack coalescence begins. After crack coalescence is complete the

two starters have joined to form a single semi-elliptical crack of known shape and depth. From this point the single crack grows in depth and width until it breaks through the specimen edges and then it quickly evolves into a straight fronted crack. The model is shown schematically in Figures 5.13, 5.14 and 5.15. Equations 5.1 and 5.2 describe the model.

$$\frac{a}{c} = e^{(k_1 a + b_1)} \quad \text{for } a < a_{\text{coal}} \quad (5.1)$$

$$\frac{a}{c} = e^{(k_2 a + b_2)} \quad \text{for } a \geq a_{\text{coal}} \quad (5.2)$$

where a_{coal} , k_1 , k_2 , b_1 , and b_2 are determined from the crack shape and depth data shown schematically in Figure 5.13. The quantity a_{coal} is the depth of the single semi-elliptical crack after it has coalesced from the two starters. The four coefficients are the slope and y-intercept of the two lines labelled in Figure 5.14. Table 5.6 gives the values of these quantities for each of the type II specimens in the present study.

The model has two distinct crack shape development regimes, one before coalescence begins and the second after it has been completed. The shape of the crack during the coalescence process is much more difficult to describe and the model does not account for shape development during this phase. The model changes from the first regime to the second at the point where crack depth of the left and right starters equals the crack depth of the single semi-ellipse into which the two flaws coalesce. This implies that the crack does not grow significantly in depth at the deepest point of the starters

while coalescence is occurring. Examination of the fracture surfaces of most of the type II specimens has shown this to be true. See for example Figure 5.8 which shows the crack depth versus cycle count relationship for specimen 02M. It can be seen that the left and right starters do not grow significantly while the crack at the centre position is growing deeper.

The other fact implied by the model is that the crack does not grow in width during coalescence. The width of the individual cracks that grew from the starters before coalescence is equal to SS - the starter spacing, and the width of the single coalesced crack is equal to $2SS$. Once again, examination of the fracture surfaces of the specimens has shown this to be a reasonable assumption for the purposes of the model. Twaddle and Hancock {71} also observed crack shape development behaviour in their study of fatigue crack coalescence in steel specimens that is similar to that assumed by the present model. Figure 5.16 (a) and (b) are graphs of aspect ratio versus crack depth for specimens 02J and 02P respectively. The figures show both data measured from the beachmarks on the fracture surface of the specimen and the two stage coalescence shape development model, note that the experimental points fit the model quite closely. The coalescence depth for 02J was four mm and so the model line for depths below this value represents equation 5.1 while those above it represent equation 5.2.

5.5 Discussion of Results

5.5.1 Crack Growth Rates - The Paris Law Constants

Typical values of the Paris law crack growth rate constants C and m obtained from

plots of da/dN versus ΔK such as Figure 5.3 are given in Table 5.7. It is evident from the table that there is a large amount of scatter in this data, the values of C ranging from 1.33×10^{-10} to 8.56×10^{-12} and the values of m ranging from 1.52 to 3.19. Broek {31} and many other authors point this out and usually quote average or baseline values for a given type of steel. In the case of the material used in the present study, the following values have been suggested by various researchers:

1) $C = 5.79 \times 10^{-12}$, $m = 3.0$

- from Scott and Thorpe {97} for a low to medium strength steel.

2) $C = 3.26 \times 10^{-12}$, $m = 3.27$

- from Thorpe et al {98} for single edge notch test data for a similar steel.

3) $C = 2.5 \times 10^{-12}$, $m = 3.25$

- from Thorpe et al {99} from compact tension and three point bending specimens.

4) $C = 2.90 \times 10^{-12}$, $m = 3.0$

- from McFadyen {29} for semi-elliptical crack bending specimens of the same steel that is being used in the present study.

The question of what values to use for the Paris law constants is important for the accurate prediction of crack growth and shape development by computer software such as the FATCRACKS program that is used in the present study. The predictions of crack depth versus life and total life can be *made* to be extremely accurate by choosing the C and m values "correctly". Many authors even advocate this and suggest ways of doing it systematically to improve the accuracy of predictions. It is felt, however, that choosing

these values after the test has been completed and the quantities to be predicted are available is a rather pointless process. If this is done, it is not the software that is being tested but rather the skill of the user in choosing the correct values for C and m .

For this reason it has been decided to use the same values for the Paris law constants that have been used for several other studies using the FATCRACKS software. This means that the results of the predictions and the accuracy of them for the present program can be compared directly to predictions performed previously using this software. The values for C and m used in the present study are as follows:

$$C = 5.36 \times 10^{-12} \text{ and } m = 3.0.$$

These values are of the same order of magnitude as the other values quoted above and are comparable to those calculated from the experimental data in the present study given in Table 5.7.

5.5.2 Comparison of Experimentally Obtained and Predicted Life

The fatigue tests were performed according to the methods described in Chapter 4 of this thesis and it is felt that the data obtained in the experimental study allow valid conclusions to be drawn. Prior to discussion of the crack growth predictions provided by the computer software it is appropriate to discuss the possible sources of experimental error, these being:

- 1) Error in the experimental crack depth measurement.
- 2) Error in the loading of the specimen.
- 3) Cyclic loading rate effects.

- 4) Error in the calculation of the crack growth rate.
- 5) Error in the calculation of the stress intensity factor range.

There is a possibility of error in the experimental measurement of the crack depth but it is felt that the method of calibration with visible beachmarks described in Chapter 3 makes this a minor consideration. Vosikovsky et al {13} claims an accuracy of 0.5 mm for the beachmark calibration method used in the present program. In any case, an error of this type would not affect a comparison to the overall life prediction, just the agreement of the experimental and the predicted crack depth versus cycle count curve. Certainly in the case of specimen 02P where the predicted life is within 2% of the actual life, the agreement between the predicted and the experimental crack growth curve is very good, Figure 5.17. This indicates that the analytical crack growth model is valid given that the experimental data is correct.

Figure 5.18 is graph of crack depth versus cycle count for specimen 02H. In this case the agreement between the experimental data and the prediction is not as close but the prediction is conservative. This conservatism is very important if this software is to be used as a tool for evaluating the safe life of structures.

It was discovered during the test program that the fatigue rig is driven by the requirement to cycle at the specified frequency rather than by the requirement to achieve the specified load. Thus it is possible, by specifying too high a cyclic frequency, to have the pump-set unable to supply enough flow to move the actuator a sufficient distance to achieve the specified load. This would have the effect of loading the specimen to a lower than expected stress range. This lower load would result in a longer life than would

normally have been achieved by the specimen.

At the time that specimens 02I and 02J were tested, a new higher capacity pump set was installed to power the fatigue rig. These two specimens were cycled at a higher frequency than had previously been used (10 Hz rather than 5 Hz) in order to save time and take full advantage of the capabilities of the new pump. It is felt that the substantially longer lives (and the rather poor agreement between the predictions and the actual test results) of these two specimens may be due to the full value of the required load not being applied because of this load "clipping" phenomenon. Subsequent tests were conducted at the lower frequency and as can be seen from Table 5.8 most of these show better agreement between the actual and predicted lives of the specimens.

The question of the cyclic frequency is interesting but it is felt would not greatly affect the results in the present program except as was noted above. The effect of frequency on the total life of fatigue specimens has been noted especially in corrosion fatigue where, in order to obtain realistic results, tests must be conducted at frequencies close to those that the structure would experience in service, Bardal {100}. In the case of an offshore structure, a typical cyclic frequency might be 0.2 Hz or a period of 5 seconds. The effect of frequency on tests in air has been found to be not nearly as marked and it is felt that the rather modest frequencies used in the present program would not cause a large effect.

The calculation of da/dN from the experimental data is a relatively simple task and it has been performed according to the standard ASTM E649 procedure {96} which gives a good balance between sensitivity and numerical stability. The crack growth rates

calculated by the simulation program are mainly influenced by the stress intensity factor range determination and the Paris law growth constants that have already been discussed. From the good agreement between predicted and actual life and crack depth versus cycle count seen in Figures 5.17 and 5.18 it can be concluded that the crack growth rate calculation and the Paris law constants are in reasonable agreement for this specimen and for several of the others.

The calculation of the stress intensity factor range is certainly the most complicated part of both the data reduction and the life prediction programs. The stress intensity factor solutions used in the software were developed by Bell et al {21-27} for fillet weld toe geometries. These are similar to the notch root of the bending specimen used in the present study in that they are of finite width and the included angle of the weld toe and the notch are both 135° . Indeed, the stress distributions through the notch root and through the weld toe region have been investigated and have been found to be quite similar. Figure 5.19 is a plot showing normalized stress distribution versus normalized plate thickness. The stresses are normalized by imposing a 1.0 MPa nominal bending stress at the toe of the weld and at the root of the notch. The thickness is normalized by dividing the depth by the plate thickness used in the analysis. The solid line is the distribution of bending stress at the weld toe of a tee plate while the points show the distribution at the root of the notch of the four point bending specimen used in the present study.

Another way of ascertaining the validity of the stress intensity factor solution used in the present program is demonstrated using data from specimens 02G and 02P in

Figures 5.20 and 5.21 respectively. These are plots of ΔK as determined by the Bell et al F.E.M. model versus the stress intensity factor determined using the Paris law equation (equation 2.7) and the values of the constants C and m quoted above in Section 5.5.1. The values of the crack growth rate, da/dN , obtained from the experimental data were used with these constants to in-effect "back out" an experimental stress intensity factor range. The line on the graphs has a slope of 1.0, thus the applicability of the analytical stress intensity factor solution is demonstrated by the proximity of the points to this line. It can be seen that the experimental and the analytical stress intensity factors agree over a wide range indicating that the F.E.M. solution is applicable to the bending specimen of the present study. The selected values of C and m are also responsible for this agreement confirming that these are reasonable values.

The FATCRACKS computer program was run on each of the specimens in the present program. The program has a choice of several different crack growth laws and methods of calculating stress intensity factors that are used in the integration of these laws. The software performs an integration of the chosen growth law simulating crack growth from a chosen initial flaw size to a final size using one of several crack shape development models. It is also possible to chose the values of the growth law constants for each run but as was indicated above this was not done so that the results from the present study can be compared more easily with previous work. For use with type II specimens the two stage crack shape development model presented in Section 5.4.2 of this thesis was incorporated into the program.

Table 5.8 gives the experimental and predicted total life to failure for all of the

specimens tested and the percentage difference between them. It can be seen that many of the predicted lives are very close to the experimentally obtained lives while others are substantially shorter. The most likely cause of the discrepancies between predicted and actual lives is simply that the fatigue process is one with a considerable degree of randomness. Therefore any prediction that is within even an order of magnitude of the actual life for a specimen can be considered a valid one.

5.5.3 Discussion of Crack Coalescence

The two major design codes that address the question of crack coalescence were referred to briefly in Chapter 2 of this thesis. These are the ASME Boiler and Pressure Vessel Code Section XI {61} and the British Standards Institution PD6493 {62}. It is interesting to examine the approach of each of these documents to the crack coalescence process and evaluate each in the light of the experimental data obtained in present study.

The ASME Section XI considers that the initial flaw grows deeper at a constant value of the aspect ratio a/c until the distance separating the inner tips of two adjacent flaws equals *twice* the depth of the deeper of the two defects. At this point the two flaws are considered to coalesce instantaneously into a single semi-elliptical defect. The width of the new single crack is equal to the sum of the widths of the two previous defects plus the separation distance and the depth is equal to the average of the two original defect depths. This approach implies a strong interaction effect since the coalescence is assumed to take place when the two inner crack tips are still a significant distance apart (10 mm separation distance for a 5 mm deep single flaw). No distinction is made in the model

for different loading conditions.

The British Standards PD6493 document approach is somewhat more complex in that the loading conditions of the cracked body have a bearing on how the coalescence of two adjacent cracks is modelled. If the bending component of the stress state of the body is less than 20% of the total applied stress the surface length of the individual cracks is assumed to be constant while growth in the through-thickness direction continues. The aspect ratio of the flaw increases until it reaches 1.0 (the crack is semi-circular) or the crack breaks through the body. In the first case the aspect ratio remains at 1.0 until either breakthrough or the coalescence criteria is met. If the stress state is such that the bending component is larger than 20% of the total then the individual flaws are assumed to be edge cracks with a long surface length. In either case, coalescence of two adjacent flaws is assumed to occur when the separation distance of the two inner crack tips is equal to the average surface length of the two defects. Thus, the PD6493 model also has a very strong interaction effect built-in.

A third model has been proposed by Iida and Kawahara {63} that has no interaction effect built in and merely recharacterizes the two adjacent flaws into a single flaw once the inner crack tips actually touch. The depth of the single flaw is equal to the deeper of the two previous flaws while its surface length is equal to the sum of their surface lengths.

The interaction effect is quite difficult to observe in a direct sense experimentally, because it requires the acquisition of growth rate data in the surface length direction (dc/dN rather than da/dN). With the present methods of crack growth monitoring the

amount of data of this type that is available is very limited. This is because of the difficulty in determining the exact location of the surface breaking point of the crack tip at a given number of cycles.

The crack shape development model introduced in Section 5.3.2 does not take into account any interaction effect and the predicted results obtained by using this model are reasonably good for most of the type II specimens. Therefore, it is felt that the interaction effect of two flaws in close proximity is small and may be neglected in estimating the fatigue life of specimens with such flaws. This suggests that the two design codes, {61-62} are over-conservative and would yield unrealistically short life predictions. This is exactly the conclusion drawn by Twaddle and Hancock {71} and Iida and Kawahara {63}.

One important difference between the work of others who have studied the coalescence process and the present study is in the area of the percentage of life consumed in the recharacterization of the two individual flaws in a single crack - the coalescence period introduced in Section 5.2. Earlier researchers have stated that the growth of the crack at the centre position once the inner crack tips have touched is extremely rapid. In fact, Twaddle and Hancock felt that the number of cycles involved in the recharacterization process is so small that it may be neglected in predicting the total life of the specimen. The experimental data from the present study for the coalescence period (Table 5.3) and the crack depth versus cycle count curves (Figures 5.6-5.9) indicate that this is not the case. The coalescence period was found to range from 28% to over 87% of the total life of the specimen. Clearly from the evidence of the present study this

portion of the fatigue life is not negligibly small.

Due to the rapid recharacterization assumed by earlier authors they state that the stress intensity factor in the cusp region between the two initial flaws is very high. Numerical studies have indicated that this is true and indeed the present study has found an enhanced crack growth rate at the centre channel of the a versus N curves for each of the type II specimens indicating that the stress intensity factor is magnified compared to other points along the crack front. By applying the growth law constants C and m to the crack growth rate data for the centre position of a type II specimen it is possible to calculate the stress intensity factor in the cusp region during recharacterization.

Figures 5.22 and 5.23 show the experimental stress intensity factor versus the crack depth for specimens 02M and 02P calculated in this manner. It can be seen that the stress intensity factor in the cusp (Middle on the Figure) is substantially greater than that of the deepest points of the initial flaws (Left and Right) at crack depths between 0 and the coalescence depth of the specimens. This accounts for the increased growth rate in the cusp (middle) region that is evident in Figures 5.8 and 5.17 which are the crack depth versus cycle count curves for these two specimens during the coalescence period.

While the data from the present study is somewhat at variance with that of earlier researchers in some details as noted above, it is clear that there is complete agreement that the two design codes being promulgated by the American {61} and British {62} authorities do not model the coalescence of adjacent flaws realistically and will result in a large under-prediction of fatigue life by imposing coalescence of the flaws much earlier in the life of the body than would occur in service.

	Spec.	Root Radius (mm)	Stress Range (MPa)	Notch Root Thickness (mm)	Initiation Life N_i (kCycles)	Total Life N_t (kCycles)
1	5A	5.0	240*	39.0	150	904
2	1A	1.0	240	39.0	27	100
3	02A	0.2	250	39.0	8	67.5
4	02B	0.2	200	39.0	40	716
5	02C	0.2	175	39.0	42	1410
6	02D	0.2	150	39.0	-	$> 9 \times 10^3$
7	02E	0.2	200	39.0	36	434
8	02G	0.2	200	39.0	25	187
9	02S	0.2	120	19.0	-	$> 15 \times 10^3$

Table 5.1 Major Test Conditions and Results for Type I Specimens.

The initiation life is defined as the number of cycles required for any point on the crack front to grow to a depth of 0.5 mm.

NOTE * - test was not conducted at constant stress amplitude.

	Spec.	Thick. (mm)	Stress Range (MPa)	Starter Crack Dimensions (mm)				Coalesced Crack Dimensions (mm)			Break Through Depth a_t (mm)
				a_i	$2c_i$	SS	$(a/c)_i$	a_{coal}	$2c_{coal}$	$(a/c)_{coal}$	
1	02F	39.0	200	0.6	3.5	23.0	0.34	3.0	42 *	0.14	4.0 *
2	02H	39.0	200	0.75	5.0	25.0	0.30	2.0	46 *	0.09	2.5 *
3	02I	39.0	200	0.60	3.0	22.5	0.40	3.0	41 *	0.15	5.0 *
4	02J	39.0	200	2.0	10.0	15.0	0.40	4.0	31 *	0.26	7.0 *
5	02K	39.0	200	3.5	10.0	25.0	0.70	4.0	50 *	0.16	6.5 *
6	02L	39.0	200	0.80	9.0	15.0	0.18	2.0	31 *	0.13	4.0 *
7	02M	39.0	200	2.5	10.0	17.0	0.50	3.2	38 *	0.17	5.0 *
8	02N	39.0	175	2.0	10.0	25.0	0.40	5.0	48 *	0.21	6.0 *
9	02P	19.0	200	0.3	10.0	30.0	0.06	4.0	57 *	0.14	4.5 *
10	02Q	19.0	170	1.0	11.0	31.0	0.18	4.0	67 *	0.12	8.5 *
11	02R	19.0	150	2.0	12.0	31.0	0.33	5.0	63 *	0.16	8.5 *

Table 5.2 Test Conditions, Starter Geometry and Coalesced Crack Shape for Type II Specimens.

NOTE * - width of coalesced crack and break-through depth are measured from beachmark data.

	Specimen	Stress Range (MPa)	Notch Root Thick. (mm)	(a/c) _i	Starter Spacing (mm)	Total Life N _f (kCycles)	Initiation Period (kCycles)	% of N _f	Coal. Period (kCycles)	% of N _f
1	02F	200	39.0	0.34	23.0	230	100	44	100	44
2	02H	200	39.0	0.30	25.0	202	40	20	100	49.5
3	02I	200	39.0	0.40	22.5	432	190	44	205	48
4	02J	200	39.0	0.40	15.0	564	10	< 2	200	36
5	02K	200	39.0	0.70	25.0	152	***	-	***	-
6	02L	200	39.0	0.18	15.0	145	12	8.3	97	67
7	02M	200	39.0	0.50	17.0	130	7.5	5.8	36.5	28
8	02N	175	39.0	0.40	25.0	218	5	2.3	190	87
9	02P	200	19.0	0.06	30.0	435	200	46	130	30
10	02Q	170	19.0	0.18	31.0	1178	200	17	840	71
11	02R	150	19.0	0.33	31.0	2269	200	8.8	750	33

Table 5.3 Type II Specimen Initiation and Coalescence Periods.

The initiation period is defined as the number of cycles required for the middle (cusp) position to grow to a depth of 0.5 mm.

NOTE *** - data unavailable.

	Spec.	Stress Range (MPa)	Notch Root Thick. (mm)	Initiation Life N_i (kCycles)	Total Life N_f (kCycles)
1	02T	170	39.0	2	159
2	02U	170	19.0	80	935
3	02V	200	19.0	31	361
4	02W	200*	19.0	1252	2112

Table 5.4 Major Test Conditions and Results for Type III Specimens.
The initiation life is defined as the number of cycles required
for any point on the crack front to grow to a depth of 0.5 mm.

NOTE * - test was not conducted at constant amplitude stress range.

	Specimen	Value of k
1	5A	0.111
2	02B	0.217
3	02C	0.217
4	02E	0.183
5	All Others	0.850

Table 5.5 Values of the Crack Shape Development Coefficient k
for Type I and III Specimens.

	Specimen	k_i	b_i	k_{ii}	b_{ii}
1	02F	-0.11	-1.00	-0.98	0.91
2	02H	-0.50	-0.83	-1.92	1.31
3	02I	-0.17	-0.81	-0.36	-0.93
4	02J	0.14	-1.20	-0.37	0.16
5	02K	-1.57	5.12	-0.27	-0.75
6	02L	0.34	-1.99	-0.48	-1.06
7	02M	-0.41	0.32	-0.61	0.28
8	02N	0.07	-1.24	-0.96	3.20
9	02P	0.40	-2.93	-1.74	4.96
10	02Q	0.12	-1.82	-0.04	-1.88
11	02R	-0.01	-1.08	-0.12	-1.22

Table 5.6 Coefficients for the Crack Coalescence Shape Development Model.

	C	m
1	3.92×10^{-12}	2.98
2	1.33×10^{-10}	2.10
3	1.91×10^{-11}	1.98
4	2.78×10^{-11}	1.80
5	1.82×10^{-12}	3.18
6	1.76×10^{-11}	2.65
7	3.65×10^{-11}	2.57
8	4.24×10^{-12}	3.19
9	3.91×10^{-10}	1.52
10	6.59×10^{-12}	2.90
11	8.56×10^{-12}	2.81
12	3.74×10^{-11}	2.09

Table 5.7 Values of the Paris Law Constants C and m Calculated from Experimental Data.

	Spec.	Type	Exper. N_f (kCycles)	Pred. N_f (kCycles)	% Diff.
1	5A	I	904*	504	44.2
2	1A	I	100	81	19.0
3	02A	I	67.5	69	-2.2
4	02B	I	716	423	40.9
5	02C	I	1410	629	55.4
6	02E	I	434	401	7.6
7	02G	I	187	165	11.7
8	02F	II	230	213	7.4
9	02H	II	202	163	19.3
10	02I	II	432	172	60.2
11	02J	II	564	135	76
12	02K	II	152	119	21.7
13	02L	II	145	84	42.1
14	02M	II	130	81	37.7
15	02N	II	218	166	23.9
16	02P	II	435	443	-1.8
17	02Q	II	1178	500	57.6
18	02R	II	2269	604	73.4
19	02T	III	159	161	-1.2
20	02U	III	935	700	25.1
21	02V	III	361	285	21.1
22	02W	III	2112*	1688	20.1

Table 5.8 Comparison of Experimental Life to Failure with the Results of FATCRACKS Life Predictions. The differences are based on the percentage of the experimental life represented by the life predicted by the FATCRACKS software.

*** NOTE** Test was not conducted at constant stress amplitude.

Figure 5.1 Weld Pass Geometry for Type III Specimens.

Figure 5.2 Stress-Life Summary of the Test Program.

Figure 5.3 Crack Growth Rate versus Stress Intensity Factor Range - Specimen 1A.

Figure 5.4 Photograph of the
Fracture Surface
of Specimen 02C.

Figure 5.5 Crack Depth versus Cycle Count - Specimen 02E.

Figure 5.6 Crack Depth versus Cycle Count - Specimen 021F

Figure 5.7 Crack Depth versus Cycle Count - Specimen 02J.

Figure 5.8 Crack Depth versus Cycle Count - Specimen 02M.

Figure 5.9 Photograph of the
Fracture Surface
of Specimen 02P.

Figure 5.10 Crack Depth versus Cycle Count - Specimen 02R.

Figure 5.11 Natural Log of Crack Aspect Ratio versus Crack Depth - Specimen 5A.
The slope of the straight line is equal to the value of k in equation 2.11.

Figure 5.12 Aspect Ratio versus Crack Depth - Type I and III Specimens.
The figure shows the crack shape development model (equation 2.11)
with a value of 0.85 for the constant k

I. Two Starter Cracks a Distance SS Apart.

Depth - a
Width - $2SC$

II. Two Adjacent Cracks.

Depth - a_{coal}
Width - SS

Coalescence Begins at This Point.

III. One Semi-Elliptical Crack.

Depth - a_{coal}
Width - $2SS$

Coalescence is Complete.

IV. Crack Breaks Through the Specimen Edges.

Depth - a_r
Width - W

V. Crack Rapidly Becomes Straight Fronted.

Figure 5.13 Schematic of the Crack Coalescence Shape Development Model

Figure 5.14 Crack Coalescence Model - Determination of the four crack shape development constants for the two crack growth regimes.

Figure 5.15 Crack Coalescence Model - Showing crack shape development before and after coalescence at crack depth a_{coal} .

Figure 5.16 Coalescence Crack Shape Development Model and Experimental Data. The solid line is the two stage crack shape development model presented in Section 5.4.2. Data shown is from specimen 02J (a) and specimen 02P (b).

Figure 5.17 Crack Growth of Specimen 02P - Comparison Between FATCRACKS Prediction and Experimental Data.

Figure 5.18 Crack Growth of Specimen 02H - Comparison Between FATCRACKS Prediction and Experimental Data.

Figure 5.19 Comparison of Normalized Stress Distribution versus Normalized Thickness for Tee-Plate Weld Toe and the Present Bending Specimen. The stress is normalized by imposing a 1.0 MPa nominal bending stress at the weld toe and notch root.

Figure 5.20 Comparison of Stress Intensity Factor Range Between the Analytical S.I.F. Calculated From F.E.M. Results for Weld Toes and the Experimental S.I.F. Calculated From Crack Growth Rates Using the Paris Law. The line on the plot has a slope of 1.0. Data is from specimen 02G.

Figure 5.21 Comparison of Stress Intensity Factor Range Between the Analytical S.I.F. Calculated From F.E.M. Results for Weld Toes and the Experimental S.I.F. Calculated From Crack Growth Rates Using the Paris Law. Data is from specimen 02P.

Figure 5.22 Experimental Stress Intensity Factor Range Calculated from Crack Growth Rates for Specimen 02M. Note the S.I.F. for the Middle position is substantially larger during the coalescence process (for crack depths between 2 and 3.5 mm).

Figure 5.23 Experimental Stress Intensity Factor Range Calculated from Crack Growth Rates for Specimen 02P.

Chapter 6 Conclusions and Recommendations

6.0 Conclusions

- 1. The alternating current potential drop method has been implemented for monitoring the growth and coalescence of fatigue cracks. As part of this effort, a study of the electrical field present in steel bars of several different sizes was made and it was found that the field is not uniform as many researchers had previously thought. This lead to the development of a crack measurement probe array consisting of three probes. This three probe set was found to give crack measurements that are accurate to within less than one mm, even when the crack front is not straight.**
- 2. An experimental study of crack coalescence has been performed using a vee-notched specimen of a Canadian made steel that is a candidate for certification as an offshore structural steel. The specimen was loaded in four point bending under constant**

amplitude sinusoidal loading.

3. The results of the initial series of tests showed that the cracks initiated and coalesced at such a shallow depth that it was not possible to study the coalescence and shape development process. This led to the testing of specimens where semi-elliptical starter cracks were electrode discharge machined (EDM) into the notch root to provide two dominant flaws that could be studied.
4. A new crack shape development model was developed based on measurements of the initial starter cracks and the pre- and post-coalescence cracks. This model also takes into account the effect of the crack breaking through the specimen edge.
5. The stress intensity factor solutions developed by Bell {21}, {26}, {27} and Bell and Kirkhope {22-25} from an extensive finite element analysis of weld toe region was shown to be applicable to the 135° notch geometry of the present specimen. Figure 5.18 shows that the experimental and analytical stress intensity factors compare extremely well over a wide range of crack depths.
6. The analytical stress intensity factor solution and the crack shape development model were incorporated into a fatigue crack growth simulation program and it was found that the total lives predicted by the program were relatively close to those obtained experimentally as shown in Table 5.8.
7. The behaviour of the specimens that were tested with the two starter cracks was found to be significantly different from that set forth in the ASME Section XI {61} and the British Standard PD6493 {62}. Specifically, the strong pre-coalescence interaction between adjacent cracks that is prescribed in both standards was not

observed in any of the specimens in the present experimental program. These two design codes and several other previous authors also state that the coalescence of two cracks into a single larger crack is virtually an instantaneous process. Data from the present program has found that the coalescence process can consume a very substantial fraction (up to 87%) of the total life of the specimen depending on the spacing of the initial starters and the stress level.

8. The two findings noted in conclusion 7 could have implications in the field of offshore structural design since following the guidelines would impose a very conservative set of rules for the treatment of existing flaws. Conservatism is of course laudable, but only if it stems from the use of a realistic model for the situation. It has been found that the two design guidelines applicable to the case of adjacent cracks are not based on a realistic model and therefore will not provide for efficient design practices.

6.1 Recommendations for Future Work

1. The present system of having discrete probes for measuring the potential drop and hence the crack depth has a weakness in that there is no way of determining the surface length of the crack. This means that surface length growth rate (dc/dN) information is unavailable and that the interaction effect between adjacent cracks is impossible to assess. A system of taking continuous readings of potential drop data across the specimen would therefore be a major addition to the experimental study of crack growth. It would also permit the stress intensity factors at the surface breaking

points of the crack to be studied in the same manner as the deepest points of the crack were in the present program.

2. A study of fatigue crack initiation of this material would also be useful and a continuous reading potential drop system would make such a study much more accurate and efficient than the present system where an initiation site could be missed if it does not occur between two active probes.
3. The stress intensity factors used in the present program have been determined from a finite element analysis. The values of aspect ratio of the semi-elliptical cracks that were analyzed ranged only as low as 0.2 (ie. a crack that is ten times as wide as it is deep). For broader cracks only two dimensional solutions are available and this accounts for the jump in S.I.F. values that occurs when the aspect ratio drops below 0.2. This problem could be addressed if a new set of crack meshes were analyzed with aspect ratios lower than 0.2. It would also be very useful to have a set of stress intensity factor solutions for the cusp region between the coalescing cracks but the mesh generation for these analyses would be very complicated.
4. The question of the choice of the Paris law growth constants C and m that were discussed in Chapter 5 is one that continues to be of interest. It is important that a careful and comprehensive experimental study be done to define these values more accurately for the candidate steel that has been used in the present study. This information is important since, if the offshore construction projects in the Maritimes and in the Arctic go ahead, a large amount of this steel will be produced and used in fatigue loaded structures. Also, it would be necessary to assess the influence of

temperature on the fatigue performance of this material since Arctic operations are a possibility.

5. A study comparing analytical and experimentally determined stress intensity factors for various crack geometries would follow naturally from many of the foregoing recommendations. With more accurate crack shape monitoring given by a continuous reading potential drop system and with better estimates of the Paris law constants C' and m , such a study would provide useful insight into the accuracy and range of validity of several existing analytical stress intensity factor solutions.

As was pointed out in Chapter 1, the service conditions for Canadian offshore structures are very harsh and failures would have extremely serious consequences both economically and environmentally. The more that we can learn about this material the better we will be able to predict its performance in service conditions.

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