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FATIGUE CRACK GROWTH IN
SEMI-ELLIPTICAL SURFACE CRACKS

by

© NEIL B. MCFADYEN

A thesis submitted to the Faculty of Graduate Studies
and Research in partial fulfillment of the requirements for
the Degree of Master of Engineering.

DEPARTMENT OF MECHANICAL AND AERONAUTICAL ENGINEERING
CARLETON UNIVERSITY
OTTAWA, CANADA

December, 1987

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The undersigned hereby recommend to the Faculty of Graduate Studies and Research, acceptance of the thesis: "Fatigue Crack Growth in Semi-Elliptical Surface Cracks", submitted by M^CFadyen, N.B., in partial fulfillment of the requirements for the degree of Master of Engineering.

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ABSTRACT

This report presents an experimental and theoretical investigation of fatigue crack growth of semi-elliptical surface cracks in mild steel plates 28 mm thick, subject to pure bending. The experimental work involved testing plate specimens with initial semi-elliptical surface flaws of aspect ratios 0.1, 0.25, 0.5 and 1.0. The specimens were tested on a High-frequency Vibrafor machine at a frequency of approximately 90 Hz in an air environment. Crack growth was monitored using the a.c. potential technique which was calibrated using beachmarks, which gave an accuracy of ± 0.2 mm.

Theoretical fatigue crack growth was predicted by a computer model which integrated the Paris growth law in both the depth and surface length directions of crack growth. Stress intensity solutions by Newman and Raju, Koterazawa and Minamisaka were used.

Comparisons of experimental and theoretical crack shape development showed good agreement for both stress intensity solutions.

Fatigue lives were predicted to within 10-30% of the experimental results.

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TABLE OF CONTENTS

	<u>Page</u>
ACCEPTANCE SHEET	ii
ABSTRACT	iii
ACKNOWLEDGEMENTS	iv
TABLE OF CONTENTS	v
LIST OF TABLES	vi
LIST OF FIGURES	vii
LIST OF SYMBOLS	x
CHAPTER 1: Introduction	1
1.1 Objectives	5
CHAPTER 2: Background of a/c Potential Technique	6
2.1 Introduction	6
2.2 Theory	7
2.3 The Microgauge Unit	9
2.4 Applications	9
2.5 Probe Design	10
2.6 Sensitivity and Accuracy	13
CHAPTER 3: Fracture Mechanics Theory	15
3.1 Linear Elastic Fracture Mechanics (LEFM)	15
3.2 Stress Intensity Factor Solutions	17
3.2.1 Introduction	17
3.2.2 General Expression for K	17
3.2.3 Stress Intensity Equation by Newman and Raju	18
3.2.4 Stress Intensity Equation by Koterazawa and Minamisaka	20
3.2.5 Finite Width Correction	21
3.3 Fatigue-Crack Growth	21
3.4 Crack Shape Development	23
3.5 Theoretical Crack Growth Prediction	23

CHAPTER 4: Experimental Setup	26
4.1 Fatigue Specimens	26
4.2 Fatigue Machine	27
4.3 Crack Growth Monitoring	28
4.3.1 Crack Depth Calibration	29
4.4 Data Postprocessing	30
CHAPTER 5: Results and Discussion	32
5.1 Summary of Specimens Tested	32
5.2 Crack Growth Monitoring Results and Discussion	33
5.2.1 Experimental Results	33
5.2.2 Discussion of Theoretical Discrepancies	34
5.2.2.1 Induction Effects	34
5.2.2.2 Crack Aspect Ratio	35
5.2.3 Crack Shape Monitoring	37
5.3 Crack Shape Development	38
5.3.1 Experimental Results	38
5.3.2 Theoretical Crack Shape Prediction	40
5.4 Discussion of Fatigue Life Results of Single Semi-elliptical Crack Specimens (#3, 4, 5, 6, 8, 9 and 10)	42
CHAPTER 6: Conclusions	48
TABLES	50
FIGURES	58
REFERENCES	115

LIST OF TABLES

<u>Table No.</u>	<u>Description</u>	<u>Page</u>
1	Quantitative Results of Induction Analysis	50
2	Chemical Composition and Mechanical Properties of Material Used	51
3	Experimental Results Summary	52
4	Initial Crack Depth Measurements for Specimens 1 and 2	53
5	Initial Crack Depth Measurements of Center Probes for Specimens 3, 4, 5, 6, 7, 8, 9 and 10	54
6	Comparison of Various Crack Growth Constants on Fatigue Life Prediction	55
7	Chemical Composition and Mechanical Properties of Material used by Thorpe et al. [8], [9] and Aboutarabi and Cowling [25]	56
8	Comparison of Experimental Conditions	57

LIST OF FIGURES

<u>Figure No.</u>	<u>Description</u>	<u>Page</u>
1	Measurement of V , V_0 for a.c. Potential Technique	58
2	Aspect Ratio Correction Curve	59
3	Block Diagram of Microgauge Components	60
4	Loop Area in Probes	61
5	Three Types of Crack Surface Displacements	62
6 a, b	Curve Fitting of Koterazawa and Minamisaka SIF Data	63, 63b
7	Regions of Fatigue Crack Growth	64
8	Crack Shape Results by Thorpe et al. [20] a/c versus a/t	65
9	Typical Probe Spacing on Specimens	66
10 a, b	Crack Growth in Specimens 1 and 2	67
11	Photograph of Specimen #3	68
12	Photograph of Specimen #4	69
13	Photograph of Specimen #5	70
14	Photograph of Specimen #6	71
15	Photograph of Specimen #8	72
16	Photograph of Specimen #10	73
17	Photograph of Specimen #7	74
18	Close-up Photograph of Specimen #7	75
19	Δd versus $\Delta V/V_0$ Specimen #1	76
20	Δd versus $\Delta V/V_0$ Specimen #2	77

21	Δd versus $\Delta V/V_0$ Specimen #3	78
22	Δd versus $\Delta V/V_0$ Specimen #4	79
23	Δd versus $\Delta V/V_0$ Specimen #5	80
24	Δd versus $\Delta V/V_0$ Specimen #6	81
25	Δd versus $\Delta V/V_0$ Specimen #7	82
26	Δd versus $\Delta V/V_0$ Specimen #8	83
27	Δd versus $\Delta V/V_0$ Specimen #9	84
28	Δd versus $\Delta V/V_0$ Specimen #10	85
29	Δd versus $\Delta V/V_0$ for Center Probes of Specimens 3, 4, 5, 6, 8, 9 and 10	86
30	Aspect Ratio Correction for Specimen #5	87
31a	Beachmark Data for Specimens 3, 4, 5, 6, 8, 9 and 10 a/c versus a/t	88
31b	Beachmark Data for Specimen 7 a/c versus a/t	88b
32	Crack Shape Near Surface Intersection	89
33	Effect of C, m on Crack Shape Development a/c versus a/t	90
34	Newman and Raju Crack Shape Prediction a/c versus a/t	91
35	Koterazawa and Minamisaka Crack Shape Prediction a/c versus a/t	92
36	Effect of Decreasing Growth in c-direction a/c versus a/t	93
37	Crack Shape Development Versus Cycles SP#9 a/c versus a/t	94
38	Crack Growth Data by Thorpe et al. [9] log da/dN versus log ΔK	95
39	Experimental Crack Growth Data Newman and Raju SIF, $\theta = \pi/2$	96

40	Experimental Crack Growth Data Newman and Raju SIF, $\theta = 0.0$	97
41	Comparison of Published Crack Growth Data with Experimental Results	98
42	a vs. N . SP # 3	99
43	2c vs. N SP # 3	100
44	a vs. N SP # 4	101
45	2c vs. N SP # 4	102
46	a vs. N SP # 5	103
47	2c vs. N SP # 5	104
48	a vs. N SP # 6	105
49	2c vs. N SP # 6	106
50	a vs. N SP # 8	107
51	2c vs. N SP # 8	108
52	a vs. N SP # 9	109
53	2c vs. N SP # 9	110
54	a vs. N SP # 10	111
55	2c vs. N SP # 10	112
56	SIF Results by Aboutarabi and Cowling [25]	113
57	Crack Growth Data by Thorpe et al. [9] log da/dN versus log ΔK	114

LIST OF SYMBOLS

a	- crack depth $\theta = \pi/2$
A_p	- loop area in probe
A_s	- slot area in crack
b	- theoretical crack depth
b_{ii}	- corrected theoretical crack depth
c	- crack length $\theta = 0.0$
C, m	- material constants
d	- crack depth
h	- height of probe
K	- stress intensity factor
ΔK	- stress intensity range
σ	- stress
σ_{ys}	- yield stress
ρ	- resistivity
μ_0	- permeability of free space
μ_r	- relative permeability
V	- active voltage reading
V_0	- reference voltage reading
R	- stress ratio
θ	- parametric angle of crack
δ	- skin thickness
Δ	- probe spacing

Chapter 1: Introduction

Fatigue crack growth and detection are major concerns with offshore drilling and production platforms. Ideally any steel structure would be designed to avoid brittle fracture, fatigue crack initiation, and growth. However, in offshore structures, particularly in the North Sea and the Canadian Offshore, it has not been possible to prevent crack initiation and growth. This is due to the combination of three main factors;

- 1) High stress concentrations at the intersections of tubular joints,
- 2) The large number of cycles, (e.g. with typical intermediate magnitude wave loading of 0.1 Hz, an offshore platform would undergo 3.15×10^6 cycles per year, in addition there are other cyclic loadings due to currents and winds),
- 3) The aggressive corrosive environment of the salt water.

Semi-elliptical surface cracks are the most common type of crack in offshore structures. They generally initiate at the tubular intersections, where stress concentrations are high and weld defects are present. The growth of these cracks poses new problems in both the design of new structures and the assessment of maintenance requirements for existing structures.

The current design practice for steel tubulars is based upon the S-N curve, stress versus the number of cycles required to obtain a fatigue failure as described in [1],

[2] and [3]. Ideally a S-N curve for every joint configuration would be required for design purposes, however, the enormous amount of time and expense to obtain such curves makes this impractical. Instead S-N curves for T-plate and T-tubular specimens are being used in combination with calculated stress concentration factors for various geometries of joints. The highest stress concentration factor or 'Hot Spot Stress' can be obtained from various techniques such as finite elements, strain gauged acrylic model testing, strain gauged welded tubular steel models or photoelastic modelling. Generally the finite element approach is the most common due to its accuracy and relatively cheaper costs.

Although the S-N approach has been in widespread use for the design of offshore platforms to date there is a growing interest in the use of a fracture mechanics model for improved life prediction and physical understanding. One of the problems with the S-N approach is that it assumes the crack initiation period occupies the major portion of the component life, whereas it has been shown by Vosikovsky et al. [4] the crack initiation period occupies only a small portion of component life (25% or less). A further complication with the S-N approach is the so called 'thickness effect' [5]. This term is used to describe the phenomenon of the S-N curve shifting downward with increasing component thickness, (i.e. for the same cyclic stress, the thicker the material the fewer cycles it can withstand). The dependence of component life on thickness has been rationalized to be caused by the variation of stress gradient with thickness. Stress gradient refers to the rate of change of stress with distance from the neutral axis in the component. The higher stress level at the same depth of crack results in a shorter life for the thicker component.

Fracture mechanics holds considerable advantages over the S-N approach, the main advantage being that effects of loading and geometry are uniquely described by the crack tip stress intensity factor 'K' (SIF). This allows data from small laboratory specimens to be applied to larger components. Another advantage is that the fatigue crack propagation, which does indeed occupy most of the component life, can be much better predicted and understood.

Life prediction using fracture mechanics can be estimated from various crack growth laws, the most common being the Paris law [6] equation which relates crack growth to stress intensity factor by the relationship,

$$\frac{da}{dN} = C(\Delta K)^m \quad (1)$$

where da/dN is the crack growth rate, C and m are constants which reflect material properties and test conditions and ΔK is the variation of stress intensity factor. The stress intensity factors for geometries where no analytical solution exists can be obtained using numerical methods such as finite elements or boundary integral equation. Experimental data of crack shape development can be used to assess the accuracy of SIF solution.

This report involves both experimental and theoretical work on fatigue crack growth and crack shape development for semi-elliptical surface cracks in steel plates subjected to pure bending. The experimental work was to provide data which could be compared to the theoretical results using the fatigue life prediction computer program by Pagotto [7].

Two solutions for stress intensity factors at surface defects were used;

- 1) Solution by Newman and Raju [8], and
- 2) Solution by Koterazawa and Miyamizaka in [9].

These solutions were obtained by fitting equations to the results of finite element analysis of various geometric configurations.

The importance of having an accurate stress intensity factor solution for predicting fatigue life is evident from equation (1) where ΔK is raised to a power of $m = 2$ to 4. For mild steel where m typically equals 3.0 a ten percent error in stress intensity factor would result in a 33% error in crack growth rate.

The accuracy of the stress intensity solutions can be evaluated by comparing theoretical crack shape development with experimental crack growth data. In this report a comparison of the two stress intensity factor solutions was made in this manner.

One of the most recent developments in experimental work in fatigue crack growth tests is the development of the Crack Microgauge Unit. The instrument uses the a.c. potential technique to monitor crack growth. The theory and background of this instrument is presented in Chapter 2. A comparison of the experimental crack growth monitoring results was made with regards to theoretical prediction of the a.c. potential theory from references [10] to [14].

1.1 Objectives

The objectives of this work can be summarized as follows:

- 1) Obtain a calibration technique for a.c. potential crack growth monitoring.
- 2) Assess the accuracy of Newman and Raju, Koterazawa and Minamisaka, stress intensity solutions by comparing experimental and theoretical crack shape development.
- 3) Investigate the accuracy of fatigue life prediction using a computer model.

CHAPTER 2: Background of a/c Potential Technique

2.1 Introduction

The alternating current (a.c.) potential method is a non-destructive testing technique for the determination of crack size and shape. Advantages over other types of crack monitoring methods include its ease of use, low operating cost and suitability for automation.

To measure the depth of a crack using the Microgauge Unit, the user need only connect the field input wires to the specimen and take two voltage readings on the specimen. One is a reference voltage near the crack and the other is the voltage across the crack. The crack depth is then easily calculated by the formula:

$$d = \left(\frac{V}{V_0} - 1 \right) \frac{\Delta}{2} \quad (2)$$

- where Δ is the probe tip spacing (10 mm)

The time to take such a measurement is considerably less than techniques such as ultrasound or eddy currents and eliminates the need for a qualified technician to calibrate equipment. For continuous crack monitoring during fatigue test, the a.c. method offers much lower operating costs than the d.c. method. This is due to the type of current field produced in the specimen. While d.c. results in a current field permeating the whole cross-section between input contacts, the a.c. field is confined to a skin at the surface of the specimen. The result is much less current is required to generate the electric field. This is particularly important

for large specimens such as tubular joints used on offshore structures where the d.c. current required could be large enough to be expensive and dangerous.

The microgauge unit features a digital signal output allowing it to be connected to a computer. This is particularly advantageous for multi-channel data acquisition and storage. In fact, for monitoring crack growth in welds of tubular joints it is a necessity since one may be using 100 probes or more.

2.2 Theory

The thickness of the current field skin is quantified by the following relationship:

$$\delta = \frac{1}{(\pi \mu_r \mu_0 \sigma f)^{1/2}} \quad (3)$$

where

μ_r = relative permeability of conductor (for mild steel $\mu_r \approx 500$)

μ_0 = permeability of free space ($4\pi \times 10^{-7}$ H/m)

σ = conductivity of conductor (for steel

$\sigma \approx 5.8 \times 10^6$ S/m)

f = frequency (Hz)

To ensure a small skin thickness for a variety of materials the Microgauge uses a frequency of 6 KHz. For steel which has a high relative permeability, μ_r , a smaller frequency could be used, however, for aluminum and titanium

which have smaller relative permeabilities a higher frequency is required. For mild steel a frequency of 6 KHz results in skin thickness of 0.1 mm and the current needed to generate the field is typically less than 1 amp.

For most conditions the 6 KHz frequency should be adequate however, the relative permeability, μ_r , of steel can vary from approximately 1 - 500 units depending on temperature, stress or chemical composition of the steel. Steel has a ferromagnetic property such that when it is stressed to the yield point the relative permeability decreases dramatically, this in effect can change the skin thickness of the surface current.

The crack depth, d , is measured by first measuring two voltages on the specimen. V_0 is a reference voltage just off the crack and V is the voltage across the crack as shown in Figure. 1. For a uniform skin thickness the voltage drop per unit length will be constant thus,

$$\frac{V_0}{\Delta} = \frac{V}{\Delta + 2d} \quad (4)$$

rearranging this;

$$d = \left(\frac{V}{V_0} - 1 \right) \frac{\Delta}{2} \quad (5)$$

It is important to know that the application of equation (1) applied only to cases of uniform field. The field can be injected using field input wires or it can be induced by a wire running across the specimen. Care must be taken so that

one avoids a partially induced field along with an injected field. The best way to do this is to have field input wires run straight away from the specimen.

The nature of the field around semi-elliptical surface cracks must also be considered. The analogous solution of the field is irrotational fluid flow around an indentation. For these cases skin depth does change slightly around the crack and the result is an underestimate of the true crack length. An aspect ratio correction curve is shown in Figure 2. This curve was derived from a two-dimensional solution of the current field around circular areas. Research is being carried out on the current field solutions for various other types of cracks to provide correction curves in order to make the method more versatile.

2.3 The Microgauge Unit

The microgauge unit basically consists of a stable constant-current a.c. source and a sensitive a.c. voltmeter. A thermistor-stabilized Wien bridge oscillator is used, which provides good amplitude and frequency stability. The output from the oscillator feeds a constant-current power amplifier, which drives current through the specimen or structure under test; connection may be made via magnets, clips or screws. The amplifier can drive loads up to about 3.5Ω . A block diagram of the main microgauge elements is shown in Figure 3.

2.4 Applications

The microgauge unit was originally designed by Dover for monitoring crack growth around welds of large tubular joints in the University College London's Lab facilities. Success

of the a.c. method has led to popular use of the microgauge as a crack monitoring device for tubular joints in offshore structures in service and in research facilities. A special microgauge unit has even been developed to be used underwater. This is an attractive feature for monitoring fixed production platforms already in use.

Another application of the a.c. method is for monitoring of cracks in the threads of bolts. The bolt is mounted in a special rig and the a.c. probe traverses the bolt as it turns.

2.5 Probe Design

The microgauge unit uses a special probe design to ensure accuracy of the surface voltage readings. It is important for the probe to keep the loop area A_p as small as possible since there is a magnetic field travelling through this loop which induces current to flow in the loop. There may also be a loop area due to the crack, A_s , as shown in Figure 4. This is also an important consideration when monitoring cracks at machined slots.

To try to quantify errors which theoretically can occur due to induced voltages in the probe loop, the following simplified model was examined. Consider a solid metal cylinder of radius R carrying an alternating current with magnitude i_0 at the surface of the cylinder. It has been shown in [3] that the measured surface voltage V_0 is;

$$V_0 = \frac{1}{\sqrt{2}} \frac{\mu l}{R^2 \pi \delta} i_0 \sin \omega t \quad (6')$$

where

- i_o = current (amps)
- p = resistivity 1.7×10^{-7} (Ωm)
- l = probe spacing (m)
- R = radius of cylinder (m)
- δ = skin thickness, equation (3) (m)
- ω = angular frequency
- t = time

and the pickup or induced voltage in the loop is given as;

$$V_p = \frac{\mu_o i_o l \omega}{\sqrt{2} 2\pi} \ln\left(\frac{h}{R} + 1\right) \sin\left(\omega t + \frac{\pi}{4}\right) \quad (7)$$

where

- μ_o = permeability of free space = $4\pi \times 10^{-7}$ H/m
- h = loop height

Table 1 contains the calculated values of V_p/V_o for values of μ_r ranging from 1 to 500 for loop heights from 1 mm to 10 mm for $R = 0.1$ m and $f = 6000$ Hz.

The results show that for low permeability, it is important to keep the loop area minimal. From Table 1 one can observe that for a material of permeability 10 and loop height the associated pickup or induced voltage is of a greater magnitude than the measured surface voltage. For materials with high permeability, such as steel it is still important to keep the loop area small but not as critical as in the case for aluminum.

Other more involved models have been developed by Mirshekar-Syahkal et al. [4, 5] to correct for induction effects caused by probe loop area and crack slot area.

The model used by Mirshekar is a modification of the basic one-dimensional solution:

$$b = \frac{\Delta}{2} \left(\frac{V_2 - V_1}{V_1} \right) \quad (8)$$

The modification accounts for induced voltages due to the probe loop area and a rectangular slot. The final form is as follows:

$$b_{11} = b \left[\frac{1 + P}{1 + S} \right] \quad (9)$$

where;

b_{11} = corrected crack length

$$P = 2 \beta \frac{L}{\mu_r} \quad \delta = \frac{A_p}{\Delta \mu_r \delta} \quad (10)$$

and;

$$S = \frac{\beta L}{\mu_r \pi} \quad (11)$$

$$\beta = \frac{\cos(\theta + \frac{\pi}{2})}{\sqrt{2} \cos(\theta + \frac{\pi}{4})}$$

θ = phase shift between the current supply and the phase sensitive detector of the microgauge

L = crack slot gap

A_p = loop area of probe

P describes the probe area induction effect and S describes the slot area induction effect.

From these equations it is clear that for a crack with $L = 0$, $b_{11} = b$ for an ideal probe. Also for a situation where;

$$\frac{A_p}{\Delta L} = 1 \quad \text{then} \quad P = S \quad (12)$$

and the two induction effects cancel so that $b_{11} = b$.

For test situations where the location of the fatigue crack is known or can be predicted from notches or stress concentrations, the use of fixed probes is often employed.

The probes are made from copper-coated steel wire. The wire is spot welded onto the specimen and then the wire is clipped to the appropriate height. The probes are typically 5 mm long and 0.5 mm in diameter. A set of probes consists of one pair to take the reference voltage and the active pair to take the voltage across the crack. They are typically spaced 10 mm apart.

2.6 Sensitivity and Accuracy

The important aspect of any crack detection unit is how small a crack it can detect and to what accuracy. The micro-gauge unit allows the user to adjust the gain of the amplifier thus giving different sensitivities. The smallest crack

detectable is 0.01 mm with a 10 mm probe. The accuracy of the measurement is influenced on the uniformity of the field, the placing of the field input contacts and type of crack measured. Generally the accuracy is around 10% for most applications. This may seem large but it is quite acceptable compared to other techniques of crack measurement. It is usually good practice to check the crack depth measurement with an optical measurement if possible. This can sometimes provide a correction factor which will make the other micro-gauge measurements more accurate.

CHAPTER 3: Fracture Mechanics Theory

3.1 Linear Elastic Fracture Mechanics (LEFM)

Linear elastic fracture mechanics is used to describe the stress field in the immediate vicinity of a sharp crack by a single parameter K called the stress intensity factor (SIF) which is proportional to stress and the square root of the crack length.

There are three basic modes of stress intensity factors which represent three types of crack surfaces displacement. These are shown in Figure 5.

I - The opening mode. The crack surfaces move directly apart.

II - The edge sliding mode. The crack surfaces move normal to the crack front and remain in the crack plane.

III - The crack surfaces move parallel to the crack front and remain in the crack plane.

Each of these modes of deformation corresponds to a different stress field in the vicinity of the crack tip. In any problem the deformations at the crack tip can be treated as one or a combination of these three modes.

Westergaard and Irwin found that the elastic stress and displacement fields in the vicinity of crack tips could be described by equations incorporating a singularity at the crack tip. The equations for the mode I type of crack are as follows:

Mode I

$$\sigma_x = \frac{K_I}{(2\pi r)^{1/2}} \cos \frac{\theta}{2} \left[1 - \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right] \quad (13)$$

$$\sigma_y = \frac{K_I}{(2\pi r)^{1/2}} \cos \frac{\theta}{2} \left[1 + \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right] \quad (14)$$

$$\tau_{xy} = \frac{K_I}{(2\pi r)^{1/2}} \cos \frac{\theta}{2} \cos \frac{\theta}{2} \cos \frac{3\theta}{2} \quad (15)$$

$$\sigma_z = 0.0 \text{ for plain stress}$$

$$\sigma_z = \nu(\sigma_x + \sigma_y) \text{ plain strain}$$

Displacements for plain strain are:

$$u = \frac{K_I}{G} \left[\frac{r}{2\pi} \right]^{1/2} \cos \frac{\theta}{2} \left[1 - 2\nu + \sin^2 \left(\frac{\theta}{2} \right) \right] \quad (16)$$

$$v = \frac{K_I}{G} \left[\frac{r}{2\pi} \right]^{1/2} \sin \frac{\theta}{2} \left[2 - 2\nu - \cos^2 \left(\frac{\theta}{2} \right) \right] \quad (17)$$

$$w = 0$$

The equations for mode II and III can be found in reference [15].

These equations are valid only as r goes to zero but they can be used to give a good approximation in the region where r is small relative to other x-y-z dimensions. These equations form the basis for calculating stress intensity factors by knowing the stress or displacement field near the crack tip.

3.2 Stress Intensity Factor Solutions

3.2.1 Introduction

Many solutions exist for mode I cracks since they are the most common mode encountered. Some of these solutions can be found in Rooke and Cartwright [16]. SIF solutions have been solved by analytical methods, experimental methods such as photoelasticity and by numerical methods such as finite elements. Of these the finite element method is a popular method since it can accommodate almost any type of geometry.

For LEFM the finite element method models the $1/\sqrt{r}$ stress singularities by special crack tip elements. The crack tip displacement data is then substituted into the Westergaard equations to calculate the stress intensity factor.

3.2.2 General Expression for K

The general form of a stress intensity factor is:

$$K = F \sigma \sqrt{\pi a} \quad (19)$$

where

σ = applied stress

a = crack length

and

$$F = F_S \cdot F_T \cdot F_W \cdot F_E \cdot F_G \cdot F_P$$

F_S = free surface correction factor

F_T = finite thickness correction factor

F_W = finite width correction factor

F_E = crack shape correction factor

F_G = geometry correction factor

F_P = plasticity correction factor

In this report two solutions for semi-elliptical surface cracks in plates subject to pure bending were used. Both of these solutions were obtained using LEFM and the finite element method. The solution by Newman and Raju [8] accounts for all of the finite geometry correction factors. The other solution by Koterazawa and Minamisaka did not include the finite width correction. To correct for this finite width correction solution by Holbrook and Dover [17] was used. The equations for this are given in a later section.

The plasticity correction factor corrects for plasticity effects by increasing the elastic crack length by an increment of half the plastic zone size. For LEFM this correction factor is not used.

3.2.3 Stress Intensity Equation by Newman and Raju [8]

The Newman and Raju solution for semi-elliptical surface cracks was developed by fitting equations to stress intensity factors obtained for three-dimensional finite element analyses. The stress intensity factor equation is of the form;

$$K_I = (S_t + HS_b) F\left(\frac{a}{t}, \frac{a}{c}, \frac{c}{b}, \phi\right) \sqrt{\pi \frac{a}{Q}} \quad (20)$$

where

- S_t = uniform tension stress
- S_b = bending stress of outer fiber
- H = boundary correction factor
- a = crack depth
- c = half surface crack length
- t = plate thickness
- b = plate half width
- Q = shape factor for elliptical crack
- ϕ = parametric angle of the ellipse

The equation is valid for;

$$0 < \frac{a}{c} \leq 1.0$$

$$0 < \frac{t}{c} < 1.0$$

$$\frac{c}{b} < 0.5$$

$$0 \leq \phi \leq \pi$$

For all configurations for which ratios of crack depth to plate thickness do not exceed 0.8 the equation is within $\pm 5\%$ of the finite element results.

3.2.4 Stress Intensity Equation by Koterazawa and Minamisaka from [9]

The equations for the Koterazawa and Minamisaka SIF solution were polynomials fitted by Scott and Thorpe [9] to Koterazawa and Minamisaka's LEFM analysis results. The curves used are shown in Figure 6. The equations are as follows:

for $\theta = \frac{\pi}{2}$, a-direction

$$K_I = M_1 \left[1 - 1.36 \left(\frac{a}{t} \right) \left(\frac{a}{c} \right)^{0.1} \right] \frac{\sigma_b}{E(k)} \sqrt{\pi a} \quad (21)$$

where

M_1 = front surface correction factor

$$M_1(\theta = \frac{\pi}{2}) = 1.13 - 0.07 \left(\frac{a}{c} \right)^{1/2} \quad (22)$$

for $\theta = 0.0$, c-direction

$$\begin{aligned} K_I = & [M_1 (1 - 0.3 \left(\frac{a}{t} \right) (1 - \frac{a}{t})^{1/2} \\ & + [0.394 \cdot E(k) \left(\frac{a}{t} \right)^{1/2} \sqrt{\frac{c}{a}}] \frac{\sigma_b}{E(k)} \sqrt{\pi a} \\ M_1(\theta=0) = & [1.21 - 0.1 \left(\frac{a}{c} \right) + 0.1 \left(\frac{a}{c} \right)^4] \sqrt{\frac{a}{c}} \end{aligned} \quad (24)$$

3.2.5 Finite Width Correction

As mentioned previously the stress intensity solution by Koterazawa and Minamisaka did not account for finite width affects. For this report the finite width correction equation by Holbrook and Dover [17] was used. The equation was as follows:

$$B_f = 1 + \left(\frac{F\left(\frac{a}{C}\right) G\left(\frac{C}{W}\right) H\left(\frac{a}{t}\right)}{(0.2745)^2} \right)$$
$$F\left(\frac{a}{C}\right) = 0.381 - 0.141\left(\frac{a}{C}\right) - 0.366\left(\frac{a}{C}\right)^2 + 0.569\left(\frac{a}{C}\right)^3 - 0.248\left(\frac{a}{C}\right)^4$$
$$G\left(\frac{C}{W}\right) = -0.0239 + 1.434\left(\frac{C}{W}\right) - 2.984\left(\frac{C}{W}\right)^2 + 7.822\left(\frac{C}{W}\right)^3$$
$$H\left(\frac{a}{t}\right) = 0.0113 + 0.323\left(\frac{a}{t}\right) + 0.749\left(\frac{a}{t}\right)^2 - 0.535\left(\frac{a}{t}\right)^3$$
(25)

These equations are only valid when $\left(\frac{a}{t}\right)$, $\left(\frac{a}{C}\right)$ and $\left(\frac{2C}{W}\right)$ are all less than 0.8.

The finite width correction factor does not effect the a/c versus a/t results since the relative values of ΔK_a and ΔK_C remain unchanged. However, the finite width correction can increase the magnitude of ΔK_a , ΔK_C by as much as 40% as a/t reaches 0.8.

3.3 Fatigue-Crack Growth

The fatigue-crack growth tests are performed under a constant amplitude cyclic load. Crack growth is monitored

along with a record of the number of cycles so that a plot of crack depth, a , versus, N , cycles can be made. From the a vs. N plot crack growth rate da/dN can be calculated. This is done by fitting a curve through the data and calculating the slope of the curve at various cyclic increments. The crack depth data and specimen geometry is then used to calculate a stress intensity range ΔK using the corresponding stress intensity factor solution. A plot of da/dN versus ΔK can then be made to describe the fatigue crack propagation behaviour of the metal. This behaviour for metals can be divided into three regions as shown in Figure 7.

- Region I - low growth rate region
- Region II - intermediate growth rate region
- Region III - high growth rate region

In region I the stress intensity range is close to the threshold value for the metal, below which no fatigue crack growth occurs.

Region II is where the majority of crack growth occurs. In this region the basic mechanism of crack growth is striation formation. Crack growth in this region can be described using the Paris crack growth law;

$$\frac{da}{dN} = C(\Delta K)^m \quad \text{from (1)}$$

m is the slope of the $\log da/dN$ versus $\log \Delta K$ curve and $\log C$ is the intercept where $\Delta K = 1$.

The values of C and m vary depending on the type of metal, microstructure, temperature, loading conditions and environment effects.

In region III, high growth rates occur and the curve moves asymptotically towards the ΔK_{IC} value for the material.

3.4 Crack Shape Development

The case of semi-elliptical surface flaws is slightly different from typical fatigue tests in that crack growth in both the depth (a) and surface (c) directions must be monitored. There are several publications which show that semi-elliptical cracks subjected to tensile or bending fatigue adopt preferred shapes. Corn [18], Pierce and Shannon [19] and Thorpe et al. [9, 20]. In particular for the bending case, results by Thorpe et al. [20] are summarized in Figure 8. Generally the data is presented in the form of a graph of aspect ratio, a/c , versus thickness ratio, a/t . If the initial crack shape is not of the preferred or equilibrium shape then it quickly adjusts to the preferred shape.

The importance of having experimental crack shape data is that it can be used to evaluate the accuracy of the theoretical ratio of stress intensity factor in the a direction to the c direction. The crack shape development is independent of the material constant C and load level, while the dependance on the material constant m is small.

3.5 Theoretical Crack Growth Prediction

To theoretically predict crack growth a computer program was developed. The program models crack growth using the Paris law equation;

$$\frac{da}{dN} = C(\Delta K)^m \quad \text{from (1)}$$

where da/dN is the crack growth rate, C and m are constants will reflect material properties and test conditions, ΔK is the range of stress intensity factor.

The computer crack growth program was used to integrate equation (1) and thus predict crack growth. The method of integration was to first select an increment of cycles ΔN from which Δa and Δc were calculated using a Runge Kutta integration scheme.

$$\Delta a = (AK_1 + 2AK_2 + 2AK_3 + AK_4)/6$$

$$\Delta c = (CK_1 + 2CK_2 + 2CK_3 + CK_4)/6.$$

where

$$AK_1 = \Delta N C(\Delta K(a_p))^m$$

$$AK_2 = \Delta N C(\Delta K(a_p + \frac{AK_1}{2}))^m$$

$$AK_3 = \Delta N C(\Delta K(a_p + \frac{AK_2}{2}))^m$$

$$AK_4 = \Delta N C(\Delta K(a_p + AK_3))^m$$

Similar equations are used for CK_1 , CK_2 , CK_3 and CK_4 .

The value of ΔN can be changed to provide small or large increments of Δa and Δc in order to maintain good accuracy. Alternatively, an increment of Δa or Δc could have first been selected, for which ΔN could be evaluated and then the other

unknown Δa or Δc . However, the results obtained by the ΔN method proved to be satisfactory and this was the method used for all the theoretical predictions.

4. Experimental Setup

4.1 Fatigue Specimens

The fatigue specimens were made from 28 mm thick steel with chemical composition and mechanical properties as shown in Table 2. Each specimen measured 150 mm wide and 400 mm long since this was the longest length that could fit in the electron discharge machine, which was used to introduce semi-elliptical notches in the specimens. Each of the ten specimens had initial notches or slits introduced to them using a slitting saw or an electron discharge machine. All the initial notches were 1 mm deep but different surface lengths were used. The ratio of the depth to the half surface length (a/c) was used to describe the shape of the cracks and this is referred to as the aspect ratio of the crack.

The first two specimens had 1 mm slits across the entire width of the plates. The slits were made by a 0.006" slitting saw. The other specimens had semi-elliptical surface flaws of aspect ratios 0.1, 0.25, 0.5 and 1.0. In addition to the single defect tests, one specimen was tested with two initial defects each of aspect ratio 0.25.

The load for each specimen was calculated so that a stress intensity range ΔK of 10 MPa $\sqrt{m}$ was obtained. This value of ΔK was selected because it was high enough above threshold stress intensity of the metal so that crack growth would occur and low enough so that a wide range of crack growth rates could be obtained throughout the test. For specimens 1 and 2, which had a slit across the entire width of the plate, the stress intensity solution for an edge-cracked plate in pure bending from Rooke and Cartwright [16] was used. For the other specimens with semi-elliptical flaws

the stress intensity solution by Newman and Raju [8] was used. For valid LEFM analysis, Irwin [21] suggested that,

$$(K/\sigma_{ys})^2 \leq 0.3\pi c$$

Duplicate experimental tests were made for all of the different aspect ratios except specimen #10 which had an aspect ratio of 0.5. The duplicate tests were to give an indication of the integrity of the experimental results, since fatigue tests are generally subject to a wide scatter band.

The stress ratios for the tests varied from 0.02 to 0.1. Although attempts to keep the stress ratio at 0.05 were made, the nature of the Vibrafor testing machine made this very difficult.

All of the tests were performed under constant loading amplitude and the tests were assumed to be over when it was no longer possible to maintain the load. This would typically occur when the crack had growth to a depth of 21 mm, which is $a/t = 0.75$.

4.2 Fatigue Machine

The fatigue testing apparatus consisted of a 10 KN capacity high frequency Vibrafor machine with a set of modified loading grips. The machine was calibrated using a proof ring and was found to have an accuracy better than 1% for static loads as shown in Appendix A1. The operating frequency of the Vibrafor ranges from 80-150 Hz depending on the type of test being performed. The tests carried out in this project were performed at approximately 90 Hz. Strain gauges were used on some of the specimens to check both the alignment and the calculated stress values.

4.3 Crack Growth Monitoring

Equipment used for crack growth monitoring included spot welded probes, co-axial cable, a Keithley Scanner and Crack Microgauge Unit. The probes used for crack growth monitoring consisted of 1/16 inch diameter copper welding wire. First the scale on the specimen was removed by grinding down the area in which the probes were required. The probes were then spot welded onto the specimen using a small capacitor type spot welder using a voltage of approximately 150 volts. The probes were then cut to a height of 5 mm. The spacing of the probes was 10 mm across the crack plane.

One set of probes consisted of:

- 1) an active probe which had one contact on each side of the crack, and
- 2) a reference probe which was located as close as possible to the active probe but had both contacts on the same side of the crack.

The spacing of the probe sets across the width of the plate varied slightly from specimen to specimen but generally they were spaced 10 mm apart at the mid section of the plate and 15-20 mm apart when close to the outside edges of the plate. One probe set was located in the center of the plate where the deepest point of the initial defect was located. Usually 9 sets of probes were used, making 18 channels to be fed to the scanner. A typical probe spacing configuration is shown in Figure 9.

The voltages being measured in this monitoring method were of the order of a few millivolts and therefore it was

important to keep noise pickup to a minimum. For this reason G14 coaxial cables were used. The length of the cables, from the specimen probes to the scanner, was kept as small as possible to further reduce noise pickup. The length was usually four to five feet. Other important considerations for keeping noise pickup to a minimum is to ensure solid connections, a high quality scanner and good grounding system. The type of scanner used was a Keithley Model Scanner featuring low voltage pickup cards and a 20 channel capacity. The grounding system used was simply a grounding wire running from the crack microgauge unit to the specimen connected by a small magnet.

4.3.1 Crack Depth Calibration

Once the specimen was placed in the Vibrafor machine and all the connections to the scanner and microgauge were made, a set of voltage readings for each probe was recorded. As explained in Chapter 2 the crack depth is related to the ratio of the active probe voltage, V , divided by the reference probe voltage, V_0 . Thus, a recording of V/V_0 was made to monitor crack growth.

An initial or base value of V/V_0 was obtained by averaging the first few readings before crack growth occurred.

Readings were taken every 25 to 100 thousand cycles depending on the test and the estimated fatigue life. Usually readings were taken every 50,000 cycles.

To get an accurate calibration of the microgauge readings versus crack depth beachmarks were used. To make a beachmark on the specimen the load range was reduced by one half for 10,000 to 15,000 cycles depending on the specimen

load and crack depth. This provided a total of 5 to 6 beachmarks on each specimen from which the microgauge readings could be calibrated with actual crack depth.

4.4 Data Postprocessing

The large amount of data generated from each specimen required some organized automatic data processing technique. Two computer programs called Post1 and Post2 were written for data processing.

Post1 required an input file of raw experiment data such as:

- 1) Specimen description
- 2) Load data
- 3) Probe geometry (spacing)
- 4) Microgauge readings (a.c. potential)
- 5) Beachmark data

From this new data, Post1 linearly interpolated between the microgauge readings at the beachmark depths to provide crack depths corresponding to other potential readings. This gave the crack depth readings for each probe at each cycle interval, as discussed in Section 5.2.

The next program Post2 was used to take the data generated from Post1 and perform two tasks.

- 1) Produce a file for plotting combinations of the following parameters: cycles, crack depth, surface crack length, thickness ratio and aspect ratio (i.e. cycles a , $2c$, a/t , a/c).

- 2) Produce a file containing crack growth rates and stress intensity factors (i.e. cycles da/dN , dc/dN , ΔK_a , ΔK_c).

Crack growth rates were calculated using the secant method [22]. This method involved fitting a straight line between the $n-1$ and $n+1$ depths and calculating a slope. Stress intensity factors were calculated using programmed solutions by Newman and Raju [8] or Koterazawa and Minamisaka in [9].

5. Results and Discussion

5.1 Summary of Specimens Tested

A summary of the specimens tested along with their initial flow sizes, final crack size, initial ΔK , stress range, stress ratio, initiation and propagation cycle data is presented in Table 3.

The final crack size refers to the point at which the constant amplitude cyclic loading could not be maintained. The initial ΔK value is the theoretical value for the load conditions using the Newman and Raju SIF solution. The number of initiation cycles refers to the number of cycles before crack growth could be detected.

Specimens 1 and 2, the single edge crack specimens, were useful in gaining experience with the crack growth monitoring equipment and the experimental procedure but unfortunately did not generate any useful fatigue life data. This was due to uneven crack growth as shown in Figure 10 a, b. Instead of the crack growing evenly across the specimen width substantially more growth on one side of the specimen occurred. This is believed to be due to uneven loading from the specimen holding grips.

All of the 7 single semi-elliptical crack specimens were successful in generating substantial data for crack growth monitoring, crack shape development and fatigue life analysis. Photos of these specimens are shown in Figures 11 to 16.

Specimen #7 was a special double crack specimen as shown in Figures 17 and 18.

5.2 Crack Growth Monitoring Results and Discussion

5.2.1 Experimental Results

Analysis of the predicted crack depths (using equation (5)) for the starter cracks in the specimens indicates that there are substantial variations and inaccuracies. For Specimens 1 and 2 which had 1 mm deep slits cut across the width of the plates the predicted depths are shown in Table 4. The initial crack depths measurements for the center probes of the other specimens are in Table 5. The variations and inaccuracies of the initial crack depth measurements are attributed to variations in probe spacing, the area affected by spot-welding the probe, the soldering of the wire onto the probe and the loop areas associated with each probe. It was found that resoldering a probe wire could result in a different V/V_0 reading.

To compare data from one specimen to another it was found to be better to compare the change in crack growth, Δd to the change in microgauge reading, $\Delta V/V_0$.

For each of the specimens tested, beachmarks were made every several mm for the purpose of calibrating the microgauge readings. Results are presented in the form of Δd versus $\Delta V/V_0$ for each of the specimens in Figures 19 to 28. The results show a wide band of scatter from probe to probe with the theoretical prediction significantly underestimating the actual crack depth in most cases.

A composite plot of Δd versus $\Delta V/V_0$ for the center probes of specimens 3, 4, 5, 6, 8, 9 and 10 is shown in Figure 29. Scatter bands around the data in Figure 29 show that crack depth could be estimated ± 1.5 mm, in most cases,

over the range of crack depth 1 to 20 mm using a single average calibration curve. Thus the percentage error is very high for small depths but reasonable for deeper cracks. If individual specimen calibration curves are used, (i.e. calibration of microgauge readings with beachmark data), the crack depth can be estimated within ± 0.2 mm using a simple linear interpolation between beachmarks. With a sufficient number of beachmarks linear interpolation is more reliable than fitting splines or polynomials because of large variation in slopes of the $\Delta V/V_0$ data and the sometimes irregular shaped pattern of data.

5.2.2 Discussion of Theoretical Discrepancies

In comparing the experimental data with the theoretical crack depth prediction based on equation (5) from Section 2 it is observed in Figure 29 that the theoretical values severely underestimate the true crack depths.

The ratio of $\Delta d_{\text{exp}}/\Delta d_{\text{theoretical}}$ ranges from 5 to approximately 1 or less over the range of crack depths in these specimens. There are several factors which are not incorporated in the simple theoretical equation (5). The following factors are discussed here:

- 1) Induction Effects
- 2) Crack Aspect Ratio

5.2.2.1 Induction Effects

Induction effects have been investigated by Mirshekar-Syahkal and Collins [14] and discussed in Section 2.5. The form of equation (9),

$$b_{11} = b \left[\frac{1+P}{1+S} \right]$$

indicates induction effects may be corrected by multiplying the theoretical crack depth b , (from equation (5)) by a constant which characterizes the loop area of the probe and the slot area due to the crack. For cracks in specimens here the slot area is essentially zero while the probe loop area could be large enough to cause significant induced voltages. However, as large as the induction effects may be they do not account for non-linearity of Δd versus $\Delta V/V_0$ data. Also quantifying the induction effects using Mirshekar-Syahkal and Collins [14] technique for calculating the induction parameters P and S , is not possible for the spot welded type probes used here.

If the differences in the theoretical and actual crack depths were in fact due to induction effects in the probes, it is difficult to believe that the differences could be so similar from specimen to specimen. In conclusion, induction effects do not account for the large differences between the experimental and theoretical data.

5.2.2.2 Crack Aspect Ratio

As discussed in Section 2.1 theoretical investigations into the effects of aspect ratio have been performed by Dover [12]. Theoretical correction factors were available in graphical form and were supplied with the crack microgauge users manual. An example of the application of the aspect ratio correction factors to data from Specimen #5 is shown in Figure 30. The results indicate the correction factors are much too small. Other results of using the corrections

showed that they were unreliable and many of the experimental points were not in the range of the theoretical data. An attempt to produce an aspect ratio correction graph of the same form as Dover [12], using experimental data showed a significant amount of scatter in lines of constant $\Delta/2c$ and also no general conformity to the theoretical graph.

The data from the specimens indicates conflicting aspect ratio effects. Evidence of an aspect ratio effect is observed in comparing data for specimens 1 and 2 to the others. Data for specimens 1 and 2, which effectively have smaller aspect ratios than the other specimens, lies closer to the theoretical prediction line for crack depths up to 15 mm. However, the last data points for specimen 2 at which time the crack is essentially straight fronted, show a divergence from the predicted depths, suggesting no aspect ratio.

Evidence of an aspect ratio effect is also shown in data for specimens 3, 4, 5, 6, 8, 9 and 10. For these specimens the aspect ratios reach approximately the same value by $a = 5$ mm (i.e. they reach the same preferred shape curve, (see section 5.3) and their aspect ratios decrease similarly with increasing depth. The data for these specimens show large deviations from the predicted depths until the crack depths reach approximately 20 mm at which time $a/c = 0.3$. This suggests that an aspect ratio effect is present. A closer comparison of data from one specimen to another, for example specimens #5 and #6 indicates that although specimen #6 had higher aspect ratios throughout its life than specimen #5 the Δ_d vs $\Delta V/V_0$ data lies below that of specimen 5 and closer to the theoretical prediction.

Crack growth monitoring results for specimen #7, which had 2 initial cracks which grew into one (Figure 25), show a definite shift toward the theoretical equation line after the sudden decrease in aspect ratio when the 2 cracks merged into one. The magnitude of the shift was 20% for a difference in a/c ratios of 0.28. However, as the crack depth reaches 20 mm there is again a divergence from the theoretical prediction.

In conclusion, there is both evidence showing an apparent aspect ratio effect and showing no aspect effect. It is possible that other factors dominate the aspect ratio effect under certain conditions as indicated by the amount of scatter in the data. For purposes of this work it is found that calibration with beachmarks provides both a simpler and accurate solution than correcting for aspect ratio and other possible effects which are difficult to quantify.

5.2.3 Crack Shape Monitoring

The technique of calibrating the microgauge readings with beachmarks gives sufficient accuracy for measurement of crack depth along the specimens, however, measurement of surface crack lengths is not as good. With typical probe spacings of 10 mm across the width of the specimen, a profile of the crack shape could be obtained, however, the surface crack length cannot be accurately measured since the angle at which the crack intersects the surface can vary. Thus, surface crack lengths could only be accurately known from the beachmarks. From these data surface crack lengths for other microgauge readings have to be interpolated. This is the largest deficiency in using the a.c. potential technique. Other optical type methods would be better suited for accurate surface crack length measurements.

5.3 Crack Shape Development

5.3.1 Experimental Results

The crack shape development of specimens 3, 4, 5, 6, 8, 9 and 10 is presented by plotting a/c versus a/t in Figure 31a, and data for double crack specimen, #7 is presented in Figure 31b.

The plot shows a definite preferred crack shape common to all specimens, which is rapidly attained despite the value of initial aspect ratio. The specimens with low initial aspect ratios grow relatively more in the direction to first increase their aspect ratios then grow relatively more in the c direction. Specimens 6 and 8 which started with initial aspect ratios of 0.9 show initial growth approximately equal in both the a and c direction, as their aspect ratio remained almost constant up to $a/t = 1.4$, then show relatively more growth in the c direction.

In the range of $0.2 < a/t < 0.6$, a/c decreases approximately linearly with increasing a/t for all specimens. The data tends to fall along 2 lines, one for specimens 3, 4, 5, 9 and 10 and another for specimens 6 and 8. The line for specimens 6 and 8 is approximated by a shift equal to 0.7 higher in aspect ratio for the range of a/t . Close attention to the crack shapes reveals that specimens 6 and 8 had much flatter shapes at the intersection of the free surface as shown in Figure 32. This may be due to the substantially higher stress range used for these specimens, $\Delta\sigma \sim 280$ MPa versus ~ 190 MPa for the other specimens. The large stress range which acts to increase the plastic zone sizes along the periphery of the crack could result in a deeper plain stress

to plane strain transition point making the crack shape flatter at the surface. The larger plastic zone size which is greatest at the surface, where there is plane stress, acts to redistribute local stresses and increase crack growth resistance. Thus, the inhibited growth in the c direction causes the higher a/c values at the same a/t than for the specimens subjected to a lower stress range.

Hodulak et al. [23] found that crack shape development was affected by load level. For specimens in tension it was found that for increasing mean stress, σ_m , the aspect ratios were lower for the same a/t . This is the opposite to the results of specimens 6 and 8, however, the Hodulak et al. results were for tension specimens for which the a/c values increased with a/t . The effect of higher σ_m caused the a/c values to stay further below the preferred shape curve for tension specimens.

The effect of load level on crack shape development is not fully understood, however, it seems the effect of load level on crack shape development does exist and it tends to inhibit the crack shape from reaching the otherwise preferred shape.

For the double crack specimen, #7, each of the starter cracks grew towards the same preferred crack shape as the single crack specimens, however, when the two cracks coalesced there was a sudden reduction in a/c . The crack then resumed growth toward the preferred crack shape as shown in Figure 31b.

5.3.2 Theoretical Crack Shape Prediction

As part of the theoretical solution analysis the effect of the C and m values on the crack shape development was investigated. The results as shown in Figure 33 demonstrate that C has no effect, which can be demonstrated theoretically, while the effect of m is very small. The results of this analysis can be extended that the theoretical solutions are also unaffected by load or finite width correction factors.

The theoretical crack shape development predictions using the Paris Growth law and SIF solutions by 1) Newman and Raju [8] and 2) Koterazawa and Minamisaka in [9] for specimens 4, 6, 9 and 10 have been superimposed on the previously discussed beachmark data as shown in Figures 34 and 35. In comparing the experimental results with the two theoretical solutions, a very good agreement is found. The Koterazawa and Minamisaka solution tends to give a better crack shape prediction for specimens starting with low initial aspect ratios while the Newman and Raju prediction gives a better prediction for the specimens starting with high values of aspect ratio.

The Newman and Raju solution predicts higher values of a/c than the Koterazawa and Minamisaka solution for all values of a/t with differences up to 15% at $a/t = 0.1$ decreasing to about 6% at $a/t = 0.6$.

Both predictions show the worst correlation with the experimental data for the initial growth of high aspect ratio specimens, 6 and 8. The experimental data shows approximately equal growth in both the a and c directions up to $a/t = 1.4$, while the predictions indicate relatively more

growth in the c direction than the a direction. This is probably due to the SIF at $\theta = 0.0$ being too high. The experimental data is also above the theoretical prediction for specimen #6 when $a/t > 0.45$, and all the other specimens for $a/t > 0.6$. This is believed to be due to increasingly larger plastic zone sizes at the crack surface which act to retard crack growth in the c direction. For specimens 6 and 8 this effect occurs at lower values of a/t because of their higher stress ranges.

To help quantify this effect the crack growth constant C for the c direction only ($\theta = 0.0$) was multiplied by 0.9^m ($m = 3.0$) which represents a 10% reduction in K. The crack growth model was then used to predict the crack shape developments for specimens 6 and 9. The results are shown in Figure 36. The results indicate that the prediction curves shift upward and now match up with the experimental data of specimens #6 for $a/t > 0.45$.

In summary both the theoretical SIF solutions give reasonably accurate ratios of $K_{\theta=\pi/2}/K_{\theta=0}$. The Newman and Raju solution is more accurate for high aspect ratios while the Koterazawa and Minamisaka solution is better for low aspect ratios. Both solutions underestimate $K_{\theta=\pi/2}/K_{\theta=0}$ for $a/t < 0.2$ and $a/c > 0.6$. Plasticity effects as the surface crack length approaches the edge of ΔK in the c direction increases the specimen and may retard growth in the c direction, and cause a flattening out effect on the a/c versus a/t curve. This phenomenon is more noticeable in a plot of a/c versus Number of Cycles as shown in Figure 37. In this figure both theoretical predictions show a dropping off in a/c as the end of the life is reached, however, the experimental data tends to flatten out indicating that growth in both a and c directions are becoming approximately equal. To

theoretically model this effect a correction factor for growth in the c direction may be required.

5.4 Discussion of Fatigue Life Results of Single Semi-elliptical Crack Specimens (#3, 4, 5, 6, 8, 9 and 10)

From the results of final crack depths shown in Table 3, a crack depth of 17 mm, or $a/t = 0.61$ was selected as an endurance limit since this was the minimum final depth achieved out of the 7 specimens.

The number of crack propagation cycles up to the endurance limit, $a/t = 0.61$, are listed in Table 6 along with the theoretical predictions of the crack growth model. The theoretical predictions were made using both the Newman and Raju [8] and the Koterazawa and Minamisaka in [9] SIF solutions for several values of C, m in the Paris law equation.

The published C and m values for ΔK in MPa/m and da/dN , dc/dN in m/cycle were as follows:

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

These were typical values appropriate for a low to medium strength steel as quoted from Scott and Thorpe [9].

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

These were results of Scott and Thorpe [20] consisting of single edge notch test data, compact tension test data and crack propagation data of semi-elliptical cracks in bending as shown in Figure 38. The composition and mechanical properties of the test material is shown in Table 7.

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

These were obtained from compact tension and 3 point bend laboratory fatigue data from Thorpe et al. [24].

4) $C = 2.9 \times 10^{-12}$, $m = 3.0$

These were experimental values of C and m obtained by a least squares curve fit to $\log da/dN$ and $\log dc/dN$ versus $\log \Delta K$ data where ΔK was based on the Newman and Raju SIF solution as shown in Figures 39 and 40. Comparison of the fitted experimental curve and published C and m curves are shown in Figure 41.

Results of the predicted endurance using the 3 published values of C and m give underestimates approximately 50% of the experimental endurance for every specimen. For $m = 3.0$ this is equivalent to a reduction in ΔK of 20%. However, when the experimental values of C and m are used, the predictions become very accurate as would be expected. Plots of a vs. N and $2c$ vs. N containing experimental data and the prediction using experimental C and m values for each specimen are shown in Figures 42 and 55.

In comparing the expected fatigue lives with the experimental data, the differences are equivalent to a 50% overestimate of crack growth rate or (for $m = 3.0$) a 20% overestimate in ΔK . There exists several possible explanations for these differences:

- 1) Experimental inaccuracy of crack growth measurement, load calibration and load calculation.

- 2) The values of ΔK from the Newman and Raju, and Koterazawa and Minamisaka overestimate the true values.
- 3) Fatigue growth rates have been retarded due to experimental test conditions.

The first and most basic investigation was the possibility of experimental inaccuracies. As far as crack growth measurement is concerned the combination a.c. potential and beachmark technique used was extremely accurate. The Vibrafor machine was calibrated using a proof-ring for static loading and was found to have an accuracy better than $\pm 2\%$. Several specimens had strain gauges to verify the load and stress calculations. It was found that the stresses were within $\pm 2\%$ of the calculations. To check the dynamic loading a digital strain gauge instrument and an oscilloscope were used which again verified the vibrator instrumentation to within $\pm 2\%$.

If the published material constants C and m are taken as correct, then the theoretical ΔK values are overestimated by approximately 20%. In Figure 31 experimental data for semi-elliptical cracks in bending obtained by Thorpe et al. [20] using the Koterazawa and Minamisaka solution, shows close agreement with the SEN and CT specimen growth rate data, which gives evidence that the theoretical ΔK values are valid.

Experimental work by Aboutorabi and Cowling [25] on the growth of semi-elliptical cracks in tension using 25 mm plates of the same material (as shown in Table 7) used by Thorpe et al. [20] and the Newman and Raju SIF solution are presented in Figure 56. The fatigue cracks were grown from initial starter notches under constant amplitude, sinusoidal,

tension to tension loading in a 1 MN servo-hydraulic test machine at a stress ratio of 0.1 and a frequency of 1 Hz. The experimental K values were obtained using the experimental crack growth rate data and the Paris law equation with $C = 2.5 \times 10^{-12}$ and $m = 3.25$ (where $\Delta K = \text{MPa}/\text{m}$, and $da/dN = \text{m}/\text{cycle}$). The C and m values were obtained from Thorpe [24]. These values of C and m are different from the values used by Thorpe in reference [20], where $C = 3.26 \times 10^{-12}$, $m = 3.27$. If the m values are taken as approximately equal comparison of the C values indicates a 23% reduction in crack growth rate (i.e. $1 - (2.5 \times 10^{-12} / 3.26 \times 10^{-12}) = 0.233$).

For $m = 3.27$ this is equivalent to an 8% reduction in ΔK for the same crack growth rate. Thus the results really indicate a K_I/K_{IEXP} to be approximately 1.18 for the C direction, ($\theta = 0^\circ$) and 1.03 to 1.13 for a direction ($\theta = 90^\circ$) up to $a/t = 0.6$. In this respect the Aboutorabi and Cowling [25] results show a similar crack growth rate versus ΔK relationship as results in this work.

From the results discussed so far it is difficult to conclude that theoretical SIF solution overestimate the true values without investigating differences in test conditions which may be affecting crack growth rates.

Factors known to affect the crack growth rates are as follows:

- 1) Stress Ratio
- 2) Frequency, Environment
- 3) Cyclic Load Waveform
- 4) Test Interruptions

The magnitude of these can be significant, as discussed by Miller et al. [26] and shown in Figure 57. A summary of the experimental conditions for work done by Thorpe et al. [9], [20], Aboutorabi and Cowling [25] and work done here, is shown in Table 8.

In comparing the various experimental results and their corresponding test conditions, effect of stress ratio seems to be the major factor to consider.

If the theoretical values of SIF_0 are taken as correct, then a comparison of crack growth rates obtained by Aboutorabi and Cowling [25] are similar to the results found here. The higher crack growth rates found by Thorpe et al. can be attributed to higher stress ratios. Although Thorpe et al. [20] found little effect of stress ratio for the CT and SEN specimens there was an effect shown for the semi-elliptic crack specimens, which cannot be taken as insignificant when it comes to life prediction.

In an earlier publication by Scott and Thorpe [9], the experimental data for semi-elliptical cracks in bending is shown to fall substantially below the SEN and CT specimen curve as shown in Figure 57. In fact, low stress ratio, 0.1, data shows more than a 20% shift in ΔK away from the SEN and CT specimen curve.

Effects of frequency on crack growth rate are not evident in comparing the results of Aboutorabi and Cowling [25], (tests at 1 Hz) and results found here, (tests at 80-100 Hz). It is well known that frequency effects are significant under corrosive environments, with low frequencies giving higher crack growth rates, however, there has been little evidence for frequency effects under normal air at

room temperature environments. During the course of this investigation it was found that results by Noack and Seifert [27] did show frequency effects at room temperature conditions on mild steel and a low alloyed quenched and tempered steel. Also work by Bowen and Forsyth [28] found significant changes in crack growth rates upon changing the load frequency in 7178-T6 Aluminum.

The possibility of test interruptions and beachmark generation substantially delaying the overall crack growth seems unlikely due to the agreement with overall crack growth rates as discussed earlier. The slight 'droop' in the a versus N curves in between the beachmarks as seen in Figures 35 to 48 is a similar effect found by Miller [26] for overnight stops. The crack growth rates were reduced after a beachmark but then returned to preferred crack growth rates.

6. Conclusions

For the objectives in Section 1.1 the following conclusions are made:

1. A calibration technique for crack measurement using the a.c. potential method was obtained. The technique which uses known crack depth values from beachmarks provides accuracy with ± 0.2 mm for the specimens tested. Future work is required for investigating the use of other techniques such as optical methods for measuring surface crack growth.
2. The assessment of the theoretical SIF solutions by 1) Newman and Raju [8] and 2) Koterazawa and Minamisaka in [9] by comparison between the experimental and theoretical crack shape predictions show good correlation with the Newman and Raju SIF solution being slightly better for growth starting with high aspect ratios and the Koterazawa and Minamisaka solution slightly better for growth starting at low aspect ratios.

Both solutions underestimate the ratio of $K_{\theta=\pi/2} / K_{\theta=0}$ for high aspect ratios and low values of a/t .

Experimental crack shape development data indicates a flattening out of aspect ratio as a/t reaches 0.7 and the surface crack length approaches the edge of the specimens. This effect is more pronounced with higher stress range.

Results of the double crack specimen indicate the cracks grow to the same preferred shape curve as single cracks, coalesce and continue again to follow preferred crack shape.

3. Fatigue Life prediction using the Paris law and the two theoretical SIF solutions used here provide a better than expected life prediction. The considerable amount of scatter in crack growth data (da/dN , dc/dN vs ΔK) requires careful selection of C and m values. The effect of stress ratio on crack growth rates can substantially alter the fatigue life prediction. Future work should be directed at investigating crack growth models incorporating stress ratio effects.

Table 1

Quantitative Results of Induction Analysis

		$\frac{V_p}{V_o} \times 100\%$
$\mu_r = 500$	$h = 1 \text{ mm}$	2.3%
	$h = 10 \text{ mm}$	22.5%
$\mu_r = 200$	$h = 1 \text{ mm}$	3.7%
	$h = 10 \text{ mm}$	35.5%
$\mu_r = 100$	$h = 1 \text{ mm}$	5.2%
	$h = 10 \text{ mm}$	50.2%
$\mu_r = 10$	$h = 1 \text{ mm}$	16.6%
	$h = 10 \text{ mm}$	159%

Table 2 - Chemical composition and mechanical properties

<u>Chemical composition:</u>		
C	0.07	0.12
Mn	1.40	1.39
Si	0.29	0.28
S	0.005	0.011
P	0.004	0.017
Al	0.045	0.027
Ni	0.25	0.15
Cr	0.25	0.17
Mo	none	≤ 0.01
Cu	none	0.15
Ti	0.012	none
V	0.075	0.053
Nb	0.022	0.041
N	0.01	-

Mechanical Properties:

Yield strength (MPa)	405
UTS (MPa)	513
Elongation (%)	29

Table 3

Experimental Results Summary

Specimen Number	Initial Crack a x 2c (mm)	Final Crack a x 2c (mm)	Initial ΔK (MPa/m)	Stress Range (MPa)	Stress Ratio R	Propagation Cycles (10^5)	Total Cycles (10^5)
1	1 x 150	—	10.0				
2	1 x 150	—	10.0				
3	1 x 17	20 x 140	10.5	179	0.086	16.82	19.82
4	1 x 17	19.5 x 136	10.9	189	0.05	18.25	24.55
5	0.8 x 7.8	19 x 120	9.5	185	0.083	18.65	19.15
6	0.9 x 2	17 x 84.5	10.1	283	0.097	4.75	5
7	0.9 x 9, 0.9 x 8	15 x 83.5	—	182	0.085	12.07	12.32
8	0.9 x 2	19 x 105	9.9	277	0.057	6.25	6.75
9	1 x 9	19 x 119	10.0	186	0.018	17.47	18.47
10	1 x 3.7	20.7 x 142.5	9.6	199	0.018	19.3	23.3

Table 4

Initial Crack Depth Measurements for Specimens 1 and 2

Probe Number	Probe Distance from Center of Plate (mm)	Specimen #1		Specimen #2	
		V/V ₀	d* (mm)	V/V ₀	d* (mm)
1	-55	1.08	0.4	1.19	0.95
2	-35	1.09	0.45	1.16	0.8
3	-15	1.18	0.9	1.11	0.55
4	+5	1.11	0.55	1.14	0.7
5	+25	1.12	0.6	1.11	0.55
6	+45	1.16	0.8	1.13	0.65
7	+65	1.17	0.85	0.97	-0.15

Note: actual depth = 1 mm for all probes

* d = predicted depth

$$d = \frac{\Delta}{2} \left(\frac{V}{V_0} - 1 \right)$$

Δ = probe spacing (10 mm) .

Table 5

Initial Crack Depth Measurements of Center Probes
for Specimens 3, 4, 5, 6, 7, 8, 9 and 10

Specimen Number		Actual Depth (mm)	V/V ₀	d* (mm)
3		1.0	1.14	0.7
4		1.0	1.07	0.35
5		0.8	1.22	1.1
6		0.9	1.025	0.125
7	crack A	0.9	1.02	0.1
	crack B	0.9	0.94	-0.3
8		0.9	0.97	-0.15
9		1.0	1.097	0.48
10		1.0	1.1	0.5

* d = predicted depth

$$d = \frac{\Delta}{2} \left(\frac{V}{V_0} - 1 \right)$$

$$\Delta = 10 \text{ mm}$$

Table 6

Comparison of Various Crack Growth Constants on Fatigue Life Prediction

Specimen Number	Propagation Cycles to $a = 17 \text{ mm}$ (10^5)	Published [9] $C = 5.8 \times 10^{-12}$, $m = 3.0$ N-R (10^5) K-M (10^5)	Published [24] $C = 2.5 \times 10^{-12}$, $m = 3.25$ N-R (10^5) K-M (10^5)	Experimental $C = 2.9 \times 10^{-12}$, $m = 3.0$ N-R (10^5) K-M (10^5)	Published [20] $C = 3.26 \times 10^{-12}$, $m = 3.27$ N-R (10^5) K-M (10^5)
3	15.68	7.1 7.29	7.11 8.3	14.15 14.51	5.87 6.11
4	17.0	6.025 6.17	6.68 6.95	12.05 12.35	4.91 5.03
5	17.52	8.39 8.15	9.75 9.47	16.75 16.75	7.08 6.87
6	4.75	3.25 2.73	4.1 2.92	6.47 5.44	2.57 2.11
8	6.0	3.45 2.93	3.85 3.16	6.97 5.85	2.78 2.28
9	16.59	7.71 7.57	8.89 8.7	15.41 15.11	6.45 6.3
10	19.39	7.97 7.23	9.2 8.36	15.91 14.44	6.76 6.07

N-R = Newman and Raju Prediction

Life up to $a/t = 0.61$

K-M = Koterazawa and Minamisaka Prediction

TABLE 7 The composition and mechanical properties of the test material to the specification BS 4360: Grade 50D [9]

	Sample plate 2149/H2	Specification
<i>Mechanical properties</i>		
Yield stress (N/mm ²)	370	345 min
Ultimate tensile stress (N/mm ²)	538	490/620
Elongation (%)	32	18 min
Charpy impact values (J)	119 at -20 °C	41 at -20 °C 27 at -30 °C
<i>Chemical analysis</i>		
C	0.17	0.18 max.
Si	0.35	0.1/0.5
Mn	1.35	1.5 max.
Nb	0.03	0.1 max.
V	0.01	0.1 max.
S	0.023	0.04 max.
P	0.037	0.04 max.
Ni	0.07	-
Cr	0.09	-
Mo	0.01	-
Cu	0.17	-
Ti	0.01	-
Al	0.05	-
B	0.001	-
Sn, Pb, Co	0.01	-

Table 8

Comparison of Experimental Conditions

	Thorpe et al. [8], [9]		Aboutarabi and Cowling [25]	M ^C Fadyen
	BS 4360, Grade 50D	BS 4360, Grade 50D	BS 4360, Grade 50D	
Material	BS 4360, Grade 50D	BS 4360, Grade 50D	BS 4360, Grade 50D	LT60 flame heat, normalized
Specimen Type	CT, SEN	Semi-elliptic Surface Crack	Semi-elliptic Surface Crack	Semi-elliptic Surface Crack
Load	Tension	3PT-Bending	Tension	4PT-Bending
Waveform	Sinusoidal	Sinusoidal	Sinusoidal	Sinusoidal
Stress Ratio	-1 to 0.85	-1 to 0.85	0.1	0.02 to 0.1
Frequency	1 - 10 Hz	1 to 10 Hz	1 Hz	80 - 100 Hz
Environment	Air Room Temperature	Air Room Temperature	Air Room Temperature	Air Room Temperature

Figure 1 Measurement of V , V_0 for a.c. Potential Technique

Figure 2 Aspect Ratio Correction Curve

Figure 3 Block Diagram of Microgauge Components

Figure 4 Probe Loop Area

Figure 5 Three Types of Crack Surface Displacements

$$\frac{K E(k)}{\sigma \sqrt{\pi a}}$$

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Magnification factors for semi-elliptic cracks in bending (b = 0) fitting results of Minamisaka & Koterazawa

Figure 6a Curve Fitting of Koterazawa and Minamisaka SIF Data [9]

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Magnification factors for semi-elliptic cracks in bending. $\theta = \pi/2$. (Fitting results of Minamisaka and Koterazawa)

Figure 6b Curve Fitting of Koterazawa and Minamisaka SIF Data [9]

Figure 7 Regions of Fatigue Crack Growth

AERE - R 10679
Development of crack shape during tests in air

Figure 8 Crack Shape Results by Thorpe et al. [20]

Figure 9 Typical Probe Spacing on Specimens

SP # 1

(a)

SP # 2

(b)

Figure 10 a, b Crack Growth in Specimens 1 and 2

Figure 11 Photograph of Specimen #3.

Figure 12 Photograph of Specimen #4

Figure 13 Photograph of Specimen #5

Figure 14 Photograph of Specimen #6

Figure 15 Photograph of Specimen #8

Figure 16 Photograph of Specimen #10

Figure 17 Photograph of Specimen #7

Figure 18 Close-up Photograph of Specimen #7

Figure 19 — SP# 1 Δ D versus $\Delta V/V_o$

Figure 20

Figure 21 - SP# 3 Δ D versus Δ V/V_0

Crack Growth Monitoring Results

Figure 22 — SP# 4 Δ D versus $\Delta V/V_o$

Figure 23 — SP# 5 Δ D versus V/V_{01}

Crack Growth Monitoring Results

Probe #

1 2 3 4 5 6 7 8 9

theoretical

Figure 24 - SP# 6 Δ D versus V/V_{o1}

Figure 25 - SP# 7 Δ D versus Δ V/Vo

Crack Growth Monitoring Results

Figure 26 — SP# 8 Δ D versus $\Delta V/V_o$

2

of/de

2

MICRO

Figure 27 — SP# 9 Δ D versus Δ V/Vo

Figure 28 — SP# 10 Δ $\square$ versus $\Delta V/V_o$

Figure 29 — Center Probes ΔD versus $\Delta V/V_0$

Figure 30 Aspect Ratio Correction for Specimen #5

Figure 31q— Benchmark data for SP # 3,4,5,6,8,9,10

Figure 31b — Beachmark data for SP #7

Figure 32 : Crack Shape Near Surface Intersection

Figure 33 — Effect of C, m

Figure 34 — Newman and Raju Crack Shape Prediction

Figure 35 — Koterazawa and Minamisaka Crack Shape Prediction

Figure 36 Effect of Decreasing Growth in c - direction

Figure 37 Crack Shape Development vs. Cycles SP# 9

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38 mm plate axial tests in air at $R = -1$ to 0.85

Figure 38 Crack Growth Data by Thorpe et al. [9]
log da/dN versus log K

Figure 39 - Experimental Crack Growth Data
Newman and Raju SIF Solution

Figure 40 -- Experimental Crack Growth Data
Newman and Raju SIF Solution

Figure 41 - Comparison of Crack Growth Data

Figure 42 - a vs. N SP # 3

Figure 43 2c vs. N SP # 3

Figure 44

a vs. N SP # 4

Figure 45 2c vs. N SP # 4

Figure 46 a' vs. N SP # 5

Figure 47 2c vs. N SP #.5

Figure 48 a vs. N SP # 6

Figure 49 2c vs. N SP # 6

Figure 50 a vs. N SP # 8

Figure 51 2c vs. N SP # 8

Figure 52 a vs. N SP # 9

Figure 53 2C vs. N SP # 9

Figure 54

a vs. N SP # 10

Figure 55 2c vs. N SP # 10

Figure 56 SIF Results by Aboutarabi and Cowling [25]
(redrawn)

Figure 57 Crack Growth Data by Thorpe et al. [9]
log da/dN versus log ΔK

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