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Symbols
\( a \)  (semi) crack length of a single crack
\( \Delta a \)  extension of crack length a
b  pitch of the two collinear cracks
dK/dt  K-rate
F  failure load
K  stress intensity factor
\( K_c \)  plane stress fracture toughness
\( K_{ic} \)  plane strain fracture toughness
t  sheet thickness
t_s  width of shear lip
W  width of specimen

Subscripts
A  related to crack tips A (fig.1.2)
B  related to crack tips B (fig.1.2)
fuit  related to full linked up crack
c  related to failure load
eff  related to crack length derived from COD measurements

Abbreviation
COD  crack opening displacement
MSD  multiple-site damage
WSFD  wide spread fatigue damage
1. Introduction

In a wide range of engineering structures, materials are subjected in service to dynamic loads with a high loading rate. The question then is whether the material response is sensitive for the loading rate. For aging aircraft structures this has become of great interest. Such aircraft can have already fatigue cracks. The question is whether the residual strength in the cracked condition will depend on the loading rate. According to the damage tolerance requirements [1] multiple-site damage (MSD) or widespread fatigue damage (WFD) should be accounted for. It implies that more than one crack can be present. In the case of MSD, a number of fatigue cracks in the same row of rivets in a lap joint of a pressurized fuselage is the classical example. It applies to the well known Aloha accident [2].

If cracks are present, the loading rate in the crack tip zone is characterized by the rate of an increasing $K$-value, i.e. by $\frac{dK}{dt}$. It can become much higher than expected if a local failure occurs in an element of the aircraft structure. If MSD is present in a single rivet row, a local failure occurs if the material ligament between two adjacent cracks fails. Also other types of local failures and impact phenomena can imply that $\frac{dK}{dt}$ for existing crack tips suddenly increases very rapidly. Under such conditions, the possibility of a reduced fracture toughness of the material, due to the high $\frac{dK}{dt}$ should be considered. This question is the basis for the present exploratory investigation.\(^{(a)}\)

A fast tensile tests on a central cracked specimen with only one crack is the most simple approach to study the loading rate sensitivity of sheet material. However, it requires a testing machine, that can apply a very high loading rate. Because the specimen should have a reasonable size, the testing machine should have a high displacement rate of the moving clamping. Such a machine was not available. As an alternative, it was expected that the condition of a high $\frac{dK}{dt}$ could also be satisfied by adopting a long specimen with two collinear cracks. Such a specimen is shown in Fig. 1.1. If the ligament AA (Fig. 1.2) between the two crack tips fails, the stress intensity factor at the outer crack tips B jumps to a much higher value. It can lead to a high $\frac{dK}{dt}$. If two symmetric cracks are used, the K-solution

\(^{(a)}\) Dynamic amplifications of the external load on an aircraft component, due to a local failure can also occur. However, this aspect is not considered in the present study, which is restricted to the material response.
is still available. In the present exploratory investigation, a long specimen (1000 mm) of 7075-T6 2 mm sheet material is used. For a short specimen the increased compliance associated with ligament failure leads to an undesirable drop of the load, which reduces the $dK/dt$ at the outer crack tips. For a long specimen this disadvantage is much smaller.

Subjects successively covered in the present report are:

- some information from the literature on loading rate effects on the material behaviour,
- dynamic energy balance aspects of fracture mechanics,
- fracture mechanics of the sheet specimen with two collinear cracks,
- experimental program and test results, including fractographic evidence.

In a discussion the significance of the experimental observations is evaluated, both with respect to the material behaviour and to practical implications for MSD problems. The report is concluded with some summarizing conclusions.
2. Dynamic material response

2.1 Information from the literature

Efforts to understand the behaviour of materials under high loading rates have increased considerably in the past 20 years. It is well established that various metals and alloys, and also nonmetallic and composite materials, show a significant change of the fracture response under increased rates of loading. An extensive review of data on the rate-sensitivity of a wide range of metals and alloys has been published by Harding [3]. It covers fcc aluminium, bcc mild steel, cph α-titanium and orthorhombic α-uranium. Under tension, torsion or punch-loading configurations, there is a significant raising of the stress-strain curve for increasing loading rates. The largest load rate sensitivity is usually found in materials of bcc structure, such as mild steel.

For aluminium alloys the rate sensitivity decreases as the strength of alloys increases. Little rate dependence was reported for the stronger alloys such as 7075-T73 [4], 7075-T6 [5] and Al-2Li [6] with a small (~3%) increase in yield and ultimate stress or slight decrease in fracture strain [7] for an increasing loading rate. Pure aluminium [8] and less strong alloys such as 2014-T6 [9] and Al-Si [8] under high load rates showed a more significant increase of the stress-strain relation. Unlike the increase in yield stress and ultimate tensile stress with increasing loading rates, fracture toughness tends to decrease under high loading rate conditions.

Klepaczko [10] performed experiments on two aluminium alloys, Al-4Cu duraluminium, no heat treatment applied, and a pure Al-Zn-Mg-Cu alloy with a heat treatment to a yield stress of 520 MPa. A wide range of loading rates was used. A substantial decrease (~30%) in fracture toughness of both alloys was observed for load rates of magnitude above $10^6$ MPa$m/s$. A small wedge-loaded CT specimen was tested for the whole range of loading rates in a closed-loop testing machine to obtain low and moderately high loading rates, 1 to $10^4$ MPa$m/s$, and the split Hopkinson pressure bar to obtain very high loading rates, up to $10^6$ MPa$m/s$. Ohlson [11] investigated fracture toughness of some aluminium alloys at higher loading rates. Centre-cracked specimens made from the aluminium alloy SIS 4332-06 (Al-Cu-Mg alloy), no heat treatment applied, and loaded with rates between 1 and 50
MPav\textdegree{}m/s displayed reduced (~30\%) fracture toughness for a moderate increase in loading rate. On the other hand, fracture toughness of ring-shaped specimens made from the Al-alloy SIS 4338-06 heat treated for maximum yield stress and loaded with the help of a split Hopkinson bar at a loading rate of 10^5 MPav\textdegree{}m/s, was only slightly higher than the fracture toughness measured at quasi static loading rates. Van Leeuwen and Schra [12] summarized their comprehensive survey of rate effects by stating that high strength precipitation hardened aluminium alloys of the Al-Cu-Mg and Al-Zn-Mg type used in aerospace technology are not likely to exhibit detrimental effects of an increased strain rate on fracture toughness. In the solution treated condition, however, these alloys may show the Portevin-Le Chatelier effect and detrimental rate effects may then be expected, as was also noticed in long time aged Al-Li alloys [13].

References concerning residual strength tests of thin cracked sheets loaded at high rates have not been found, except for reports of the National Aerospace Laboratory NLR in Amsterdam. Broek and Nederveen [14] found no effect on the critical stress intensity factor of aluminium alloy 2024-T3 (alclad) and 7075-T6 (clad). They varied the time of loading to failure between 0.4 seconds and 30 minutes (loading rates between 2x10^2 MPav\textdegree{}m/s and 4x10^{-2} MPav\textdegree{}m/s), which would correspond to that of gust loading in flight and to cabin pressurization respectively. Although no effect was found on the fracture stress and the critical crack length, a decrease with increased loading rate was observed for the stress to initiate a crack from a saw cut. In two recent reports concerning fatigue and fracture of high strength Al-Li alloy sheets, Wanhill et al. [15] and Wanhill and Schra [16] predict a sensitivity to dynamics effects in both damage tolerant 2091 Al-Li alloys and high strength 2090 Al-Li alloys.

Summarizing results of high strength precipitation hardened aluminium alloys of the Al-Zn-Mg type under high loading rates of magnitude \(\geq 10^4\) MPav\textdegree{}m/s, it may be expected that the 7075-T6 aluminium alloy, which is investigated in this work, will show a very slight increase of the tensile yield stress and a decrease in fracture toughness and fracture strain. It implies that crack tip plastic zone sizes at high loading rates could be smaller. Assuming that crack tip plasticity usually qualifies the plane-strain/plane-stress transition behaviour and fracture modes [15], it might be expected that there is a loading rate sensitivity of the
residual strength of 7075-T6 aluminium alloy thin sheet loaded with K-rates above $10^6$ MPa/m/s.

2.2. Energy balance approach

In the energy balance approach several contributions to the crack driving energy and to the crack growth resistance energy should be considered. Under dynamic loading, kinetic energy and reflected stress waves can be important. As discussed in the previous chapter, it is also possible that the material plasticity behaviour is loading rate sensitive. Moreover, the fracture mechanism at high loading rates can be different. In the present investigation long sheet specimens are used (Fig. 1.1). An essential question is whether the dynamic conditions are representative for the structural problem indicated in the introduction.

With respect to the material behaviour, a different fracture mechanism has been reported for steel under high loading rates. A cleavage type failure mechanism can occur at low temperature and a high dK/dt [15,18], instead of the slip dominated mechanism during quasi-static conditions. However, for Al-alloys such a different mechanism has not been reported. Plasticity and void formations in the plastic zone around inclusions are the characteristic phenomena for crack extension in Al-alloys. Because the void formation is also a function of the plastic deformation, a rate effect should originate from loading rate effects on plastic deformation. In 7075-T6 the plastic zones are considerably smaller than for the more ductile 2024-T3 alloy. However, without any doubt, the energy required for creating the (moving) plastic zone is the predominant contribution of the material crack growth resistance. The effect of this rate-dependent material plasticity is a local phenomenon occurring in the crack tip plastic zone. It may well be expected to be similar for a specimen and for the same sheet material in a structure.

Under quasi-static conditions, the crack driving energy consists of the work applied to the specimen and the potential energy stored in the specimen. Under dynamic loading conditions, kinetic energy and reflected stress waves should be considered. The kinetic energy associated with a running crack is a difficult issue. It does affect the energy balance, and it can be incorporated in the crack driving force, as discussed e.g. in [17-19]. Since we
are interested here in the onset of unstable crack growth, it may well be left outside the
analysis of the results of the present investigation.
Reflected stress waves on the other hand, are depending on the shape and the size of the
specimen. In our specimens, the waves will return to the crack tip areas in time intervals
less than 0.1 ms, assuming a characteristic wave velocity of 5000 m/sec. For the present test
set up the stress waves will be ignored. The waves must be generated during crack
extension. The wave energy should be assumed to be significantly smaller than the other
energy increments (plasticity, global potential energy). The preliminary conclusion then is
that any loading rate effect found in the sheet specimens, should be related to loading rate
dependent plasticity.
3. Fracture mechanics of a sheet specimen with two collinear cracks

3.1 The specimen and the K-solutions

In view of K-solutions it was desirable to have two symmetric collinear cracks. For this purpose two symmetric saw cuts were made first. Each saw cut was obtained by drilling a central hole (1 mm diameter) from which the saw cuts were applied. The saw cuts were extended by fatigue at both ends with approximately 1 mm as a maximum, see Fig. 1.2. It was considered to be essential to have fatigue crack tips in order to obtain meaningful fracture results. A low fatigue load was used during the precracking to avoid shear lips ($K_a \sim 10 \text{ MPa}\sqrt{\text{m}}$, $R = 0.1$, frequency $\sim 5 \text{ Hz}$). For all specimens, the distance of the far tips B was approximately constant and equal to:

$$2a + 2b = 2a_{\text{full}} = 60 \text{ mm} \quad (3.1)$$

The distance between the near tips A was modified by selecting different values for b. The ratio $a/b$ could vary in the interval $0 < a/b < 1$ as necessary. For $a/b = 1$ two collinear cracks become one single crack with a tip to tip length $2a_{\text{full}}$. For a constant value of $a_{\text{full}}$ (Eq. 3.1), the ratio $a/b$ completely describes the geometric configuration of the two equal collinear cracks. Due to scatter of the small fatigue crack extension, the two collinear cracks did not have an exactly equal length. However, the differences were less than 0.5 mm in the worst case, and equal to 0.2 mm on the average. The differences are neglected.

The ligament width, $AA$ in Fig. 1.2 ($AA = 2b - 2a$), should be selected in such a way, that a large increase of the stress intensity factor $K_B$ at the outer crack tips does occur at the moment of ligament failure. Assuming that ligament failure under an increasing load is a dynamic phenomenon, occurring in a very short time interval, a rapid increase of $K_B$ will be obtained. If stable crack extension does not occur before ligament failure, the sudden increase of $K_B$ can be written as:

$$\Delta K_B = K_{\text{full}} - K_B \quad (3.2)$$

where $K_B$ is the K-value at tips B before ligament failure and $K_{\text{full}}$ the K-value after ligament failure. The geometry correction factors for the K-values in Eqs.(3.3) to (3.5) are given in
If stable crack extension is ignored, ligament failure occurs if $K_A$ reaches a critical value $K_c$ (plane stress fracture toughness), $K_A = K_c$.

The normalized values, calculated with the equations given in Appendix B, are $K_B/K_c$, $K_{full}/K_c$, and $\Delta K_B/K_c$. These values are presented in Fig. 3.1 as a function of $a/b$. It should be noted that for $a/b \rightarrow 1$, the ligament width goes to zero, and ligament failure will occur at a very low load. As a consequence, all normalized $K$-values also go to zero, which is of little practical interest. Figure 3.1 suggests that a high $\Delta K_B$, which is desirable for the purpose of the present investigation, requires a low $a/b$, i.e. a relatively large ligament width. However, then ligament failure requires a high stress level $\sigma$, and it should be expected that some stable crack extension will occur.

### 3.2 Stable crack extension

To approach the real conditions some stable crack growth increments $\Delta a_A$ and $\Delta a_B$ at both tips A and B should be taken into account:

\[
\begin{align*}
    a &= a_o + (\Delta a_A + \Delta a_B)/2 \\
    b &= b_o + (\Delta a_B - \Delta a_A)/2 \\
    a_{full} &= a_{full,o} + \Delta a_B
\end{align*}
\]

where $a_o$, $b_o$ and $a_{full,o}$ are the initial dimensions. Assuming that the stable crack extension initially starts as a tongue-shaped crack extension of the fatigue crack (as observed on the fracture surfaces (see chapter 5), an estimate of $\Delta a_A$ and $\Delta a_B$ can be made by adopting the Vlasveld-Schijve model [21]. This model was developed for the initial stable crack extensions under an increasing load in thick plates. It must be modified for thin sheet material, which is done in Appendix A. The modification yields a relation between the stable
crack growth increment $\Delta a$ and stress intensity factor $K_{\Delta a}$ responsible for this increment. An inverted power-law approximation of this function (Eq. A8) was used for the estimation of stable crack increments at tips A and B:

$$\Delta a_A = \left( \frac{K_A - K_{Ic}}{17.9} \right)^2 \quad (3.7)$$

$$\Delta a_B = \left( \frac{K_B - K_{Ic}}{17.9} \right)^2 \quad (3.8)$$

($\Delta a$ in mm, K in MPa$\sqrt{m}$)

For $K < K_{Ic}$ it is supposed that $\Delta a_A = \Delta a_B = 0$. A substitution of equations (3.7) and (3.8) into the equations (3.6), and these equations into the equations (3.3)-(3.5) leads to a complex system of equations. A numerical solution was obtained, which is plotted in Fig.3.2 as a function of $a/b$. Apparently, $\Delta K_B$ now shows a behaviour, which is significantly different from the result shown in Fig.3.1. Values of $\Delta K_B$ are much smaller in Fig.3.2 due to the stable crack extension.

The significant issue is that ligament failure is followed by a jump of $K_B$ to $K_{full}$. If $K_{full}$ exceeds $K_c$ complete specimen failure will occur immediately. However, if $K_B$ jumps to a $K_{full}$ value lower than $K_c$, ligament failure need not occur immediately. It may require a further load increase. According to Fig.3.2 this could occur for $a/b > 0.67$ ($K_{full} = K_c$ at $a/b = 0.67$). The maximum $\Delta K_B/K_c$ to be obtained at $a/b = 0.67$ according to Fig.3.2 is 0.28. If $a/b$ is significantly larger than 0.67, the load must be increased by the testing machine to fail the specimen. A dynamic effect on $K_c$ will probably not be detected. If $a/b$ is significantly smaller than 0.67, the load of the testing machine at ligament failure is larger than the failure load of the specimen with a single full crack ($K_{full} > K_a = K_c$). A dynamic effect on $K_c$ will remain hidden again. A dynamic effect can only be observed for $a/b$ values for which ligament failure and specimen failure require approximately the same load. The tests should therefore cover a range of $a/b$ values around the critical value for which $K_{full} = K_c$. The high $K$-rate after ligament failure may then reveal a dynamic effect on the fracture toughness.
4. Experimental program

4.1 Residual strength test

The specimens (Fig. 1.1) were cut from 2 mm 7075-T6 bare aluminium alloy sheets of basic dimensions 2x1200x2500 mm. The rolling direction was parallel to the longitudinal axis of the specimen. Table I shows the composition limits in weight percent and the artificial ageing treatment. The mechanical properties of thin sheets for the longitudinal rolling direction are given in Table II.

A series of 13 residual strength tests was carried out on an MTS servohydraulic testing machine with a maximum load capacity of 1000 kN, a compliance of 4.5x10^-4 mm/kN, and a maximum actuator velocity of 50 mm/s. The load on the specimen and the crack opening displacement (COD) were recorded as a function of time. These records were used for the evaluation of the crack driving force and to determine the moment of ligament failure.

A survey of the residual strength tests is given below.

<table>
<thead>
<tr>
<th>specimen</th>
<th>number of tests</th>
<th>a/b</th>
<th>loading rate</th>
</tr>
</thead>
<tbody>
<tr>
<td>single crack</td>
<td>2</td>
<td>1</td>
<td>quasi-static</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>1</td>
<td>dynamic</td>
</tr>
<tr>
<td>two collinear cracks</td>
<td>9</td>
<td>0.63 to 0.82</td>
<td>dynamic</td>
</tr>
</tbody>
</table>

More details about the crack lengths are given in Table III.

Two nominal loading rates were applied to the specimens. A slow quasi static loading rate was generated by a ramp command 0-100 kN in the load channel up to failure of a specimen with a loading rate 10 kN/sec. For a specimen with a single crack of 2a = 60 mm, it corresponds to a stress intensity rate \( \frac{dK}{dt} = 10 \text{ MPa}\sqrt{\text{m/s}} \). Tests with this loading rate are referred to as quasi-static tests.

In the dynamic tests a fast loading rate was generated by a square-step command 0-200 kN in the load channel up to failure of a specimen. It gives the maximum actuator velocity, which resulted in a loading rate of 1000 kN/s. It corresponds to a nominal stress...
intensity rate of $v_K = 10^3$ MPa$\sqrt{\text{m}}$/s.

In the tests with two collinear cracks, failure at the tips A occurred suddenly, and the stress intensity at the crack tips B increased abruptly. The maximum stress intensity rate can be evaluated with the expression:

$$v_{K,\text{max}} = \frac{\Delta K_B}{\tau_{\Delta K}}$$

(4.1)

where $\Delta K_B$ is either $K_{\text{full}} - K_B$ ($a/b > 0.67$ in Fig.3.2) or $K_A - K_B$ ($a/b < 0.67$ in Fig.3.2), and $\tau_{\Delta K}$ is a time interval during which the stress intensity at tip B increases by $\Delta K_B$. $\tau_{\Delta K}$ was in all tests approximately constant and equal to $\tau_{\Delta K} = 0.4$ ms. If the stress intensity increment $\Delta K_B$, is not high enough to cause failure of a specimen immediately, there can be an additional quasi static increase in stress intensity up to the final failure of the specimen at $K_c$. Then it is more correct to define a mean value of the rate at dynamic loading:

$$v_K = \frac{\Delta K_f}{\tau_f}$$

(4.2)

where $\Delta K_f = K_c - K_B$ is the total increase of the stress intensity at tip B, and $\tau_f$ is the period for going from $K_B$ to $K_c$. Values of $\tau_f$ were measured from COD vs. time curves. The increments $\Delta K_f$ were evaluated by analyzing crack driving force-curves, described later. $\tau_f$ and $v_K$ for individual tests are presented in Table IV.

Load and crack opening displacement (COD) signals were recorded by the MTS TEST STAR control system and in real time stored to the memory of a PC with a sampling frequency of 5 kHz. The load signal was measured by the 1000 kN MTS load-cell placed below the lower grip of the specimen. The COD was measured by a special optical device described later. Examples of load and COD data as saved during the tests are shown in Figs.5.1. The load $F$ was used for calculating the remote stress $\sigma$ (gross stress) as:

$$\sigma = \frac{F}{W.t}$$

(4.3)

where $W.t$ is specimen cross section.
During tensile loading of a specimen with a single central crack, stable crack growth under increasing load could be observed. After some crack extension $\Delta a_c$, the crack becomes unstable at a stress $\sigma_c$ and a complete failure occurs. This involves two possible definitions of fracture toughness:

$$K_{c0} = \sigma_c \sqrt{\pi a_0} \cdot f_w(a_0, W) \quad (4.4)$$

and

$$K_{cc} = \sigma_c \sqrt{\pi a_c} \cdot f_w(a_c, W) \quad (4.5)$$

where $f_w(a,w)$ is the width correction factor (see Appendix B). $K_{c0}$ is the apparent critical stress intensity factor or engineering fracture toughness, related to the initial crack length $a_0$ and the stress at failure $\sigma_c$. The true fracture toughness for fracture, $K_{cc}$, employs the crack length at failure: $a_c = a_0 + \Delta a_c$. The value of $a_c$ was derived from the fracture surface as the crack length at the end of the tongue. Because this might not be entirely satisfactory, the ASTM standard procedure [25] was also adopted. It is based on the effective crack length $a_{eff}$ at $\sigma_c$ as obtained from a compliance measurement (COD). The definition of fracture toughness then is:

$$K_{c,eff} = \sigma_c \sqrt{\pi a_{eff}} \cdot f_w(a_{eff}, W) \quad (4.6)$$

which is called the effective fracture toughness.

4.2 Crack opening displacement and effective crack length

A simple COD-meter for the saw-cut opening displacement was designed. The sketch in Fig.4.1 shows the arrangement of the device. A wide light stream falls from a stable light source perpendicularly on the specimen surface in the region of the saw-cut slot. Only a narrow part of the light stream passes through the slot. This part is again constrained by a horizontal gutter of the photo-cell window (Fig.4.2). The photo-cell measures the light intensity which is proportional to the opening of the slot. The window determines the part of the saw-cut slot used for the measurements. A compromise between a sufficiently small part of the saw-cut and an acceptable level of noise of the photo-cell signal $U_{COD}$ was found for $5 \text{ mm}$ of the gutter slot width. Some other dimensions are given in Fig.4.2. The
 calibration curve of the COD-meter in Fig.4.3 shows an extremely good linearity, represented by the equation:

\[ \text{COD [mm]} = 1.04 \times (U_{\text{COD}} - U_0) \ [V] \] (4.7)

where \( U_0 \) is the photo-cell signal for the saw-cut in the unloaded specimen. A constant position, \( x_{\text{COD}} \), on the crack was selected for all tests (Fig.4.2), which is:

\[ x_{\text{COD}} = a_{\text{full}} - 10 \text{ mm} = 20 \text{ mm} \] (4.8)

Assuming an elliptical shape of the crack mouth, \( \text{COD}_x \) measured at \( x_{\text{COD}} \) can be transformed to the maximum COD at \( x_{\text{COD}} = 0 \) (the center of the crack) by the formula:

\[ \text{COD} = \text{COD}_x \times \frac{a_{\text{full}}}{\sqrt{a_{\text{full}}^2 - x_{\text{COD}}^2}} \] (4.9)

An analytically developed expression, derived by Eftis and Liebowitz and used in the ASTM "clip-gauge" procedure [22] for calculation of the centre cracked specimen compliance, was used for evaluation of the effective crack length \( a_{\text{eff}} \). Using data of load vs. COD records, the effective crack length can then be obtained by the relation:

\[ \text{COD} = \frac{\sigma}{E} \sqrt{\frac{A}{\sin A}} \left[ \frac{2}{B} \arccosh \left( \frac{\cosh B}{\cos A} \right) - \frac{1 + \nu}{\sqrt{1 + \left( \frac{\sin A}{\sinh B} \right)^2}} \right]^{2Y} (4.8) \]

with:

\[ A = \frac{\pi a_{\text{eff}}}{W}; \quad B = \frac{\pi Y}{W} \] (4.11)

where \( Y \), the originally half span of the clip gauge [22], was taken as half the height of the saw-cut slot, \( Y = 0.25 \) mm.
5. Results

5.1 Residual strength

As an illustration, Figs. 5.1a to 5.1e show the load and COD data stored in the computer during tests of five specimens. Fig. 5.1a is for a specimen with a single crack, and Figs. 5.1b to 5.1e for specimens with two collinear cracks, but different ligaments. The load necessary for fracture at tips A is denoted by $F_A$, and the maximum load for fracture of the specimen by $F_c$. The load $F_A$ was detected by a significant step in the COD-curve, which represents a fast opening of the crack after ligament failure. For a decreasing $a/b$ (i.e. an increasing ligament size), the step in the COD-curve occurs at a higher load $F_A$, closer to $F_c$ (compare Figs. 5.1b and c). For $a/b = 0.628$ (Fig. 5.1e) failure at the crack tips A occurred at a load which is beyond the critical load for the full crack. The value of $F_c$ could thus not be separated from the value of $F_A$ in this test. Results for the loads $F_A$ and $F_c$ of all specimens, and the corresponding stress levels $\sigma_A$ and $\sigma_c$, are compiled in Table III. Values of the engineering fracture toughness $K_c$ (Eq. 4.4) are shown as well. $K_c$ for $a/b = 0.628$ is given in brackets because this value is calculated with $\sigma_A$ instead of $\sigma_c$, since $\sigma_c$ remains unknown.

The critical loads could also be determined from the load-COD curves. Examples are shown in Figs. 5.2 again for five different $a/b$-values. There are three important breaks in the load-COD curves. COD$_A$ is the crack opening at the moment of instability at the tip A and corresponds to stress $\sigma_A$. The full opening of one crack B-B, after the two cracks have linked up, is designated by COD$_{\text{full}}$. The critical value of crack opening, COD$_c$, occurred at fracture instability of the specimen at stress $\sigma_c$. The value of COD$_{\text{full}}$ approaches COD$_c$ for a decreasing $a/b$ (increasing ligament width), and COD$_{\text{full}}$ and COD$_c$ almost merge at $a/b = 0.674$ (Fig. 5.2d). At $a/b = 0.628$ only COD$_A$ could be measured. The flat part of the load-COD curves for COD $> COD_c$ represents the final fracture of the specimen. It confirms the constant load on the specimen during unstable crack growth.

The critical stresses $\sigma_A$ and $\sigma_c$ are plotted versus $a/b$ in Fig. 5.3. For $a/b$ $\sim$ 0.7 the $\sigma_c$-values are almost 10% below the quasi-static value. Values of the engineering fracture toughness
in Fig. 5.4 decrease in the same interval of \( a/b \) as in Fig. 5.3. In other words, the results indicate a reduced fracture toughness during linking up of the two collinear cracks. It indicates a lower dynamic fracture toughness when \( dK/dt \) is amplified by the linking up process. For \( a/b > 0.7 \) both, the residual strength \( \sigma_c \) and the fracture toughness \( K_c \), tend to return to the quasi-static level. In this \( a/b \) range, ligament failure is not immediately followed by specimen failure (see e.g. Figs. 5.1b and 5.2b). As a result the \( dK/dt \) at the moment of complete specimen failure at crack tips B is lower than for the specimens with \( a/b \sim 0.7 \). Actually, the K-rate becomes similar as for the specimens with a single crack, and so does the \( K_c \)-value.

For \( a/b < 0.7 \) the two collinear cracks behave as two independent cracks. Fracture at tips A occurs simultaneously with fracture of tips B. In this case a dynamic effect of the abruptly opened crack tips B can not be observed.

5.2 Crack driving force

As pointed out before the crack driving force can also be expressed by using the effective crack length \( a_{\text{eff}} \), rather than the apparent crack driving force. This is possible here because \( a_{\text{eff}} \) was obtained by using the ASTM compliance method and the COD results obtained by the photo cell measurements. The reliability is confirmed by comparing the initial parts of the load/COD measurements of two specimens in Fig. 5.5. The agreement between the results and the calculated slope based on the COD measurements is encouraging. The effective values of the characteristic crack length values (\( a_{B,\text{eff}}, a_{\text{full,eff}}, a_{c,\text{eff}} \)) and the corresponding effective K-values (\( K_{B,\text{eff}}, K_{\text{full,eff}}, K_{c,\text{eff}} \)) are compiled in Table IV. Since the time increment \( \tau_f \) required for going from \( K_B \) to \( K_c \) could also be derived from the records, the average K-rate could be calculated, see the last column of Table IV.

The effective fracture toughness \( K_{c,\text{eff}} \) is plotted in Fig. 5.6 as a function of \( a/b \). In the interval of \( a/b \sim 0.7 \) it shows again a decrease by about 10% if compared to the quasi-static value. The load rate sensitivity of the effective fracture toughness is shown in Fig. 5.7. There is a systematic decrease for load rates above \( 2 \times 10^3 \) MPa\( \sqrt{\text{m/s}} \). From the trend in this figure one might wonder, what will happen if still larger \( dK/dt \) rates could be realized.
The critical crack growth increment $\Delta a_{\text{c, eff}} = a_{\text{c, eff}} - a_{\text{full}}$ was evaluated as the difference between the effective crack length at specimen instability $a_{\text{c, eff}}$ and the initial full crack length $a_{\text{full}}$. Fig. 5.8 shows the increment in relation to the effective fracture toughness $K_{\text{c, eff}}$. Apparently, the critical crack growth increment decreases systematically with a decreasing effective fracture toughness. It also implies, that dynamic fracture occurs at a shorter crack increment than it was found for quasi-static loading.

5.3 Fractography

Four specimens were investigated in the scanning electron microscope (SEM) to study fracture surfaces created under both medium and dynamic rates of loading. Variations of fracture surface topography were recognized, both in the direction of the running crack and in the direction perpendicular to the running crack (across the specimen thickness) for all loading rates. The significant parts of the fracture surface from the crack origin ($a = 0$) up to a crack extension of $\Delta a = 40$ mm are shown in Figs. 5.10 - 5.13. For $\Delta a$-values larger than 40 mm, the fracture surface did not change significantly. Complementary information was also obtained from Figs. 5.13 and 5.15, which show cross sections of the fracture surface of two specimens already shown in Figs. 5.10 and 5.13. The cross sections were made in the thickness direction perpendicular to the crack growth direction. These cross sections were made at four distances from the crack origin ($a = 1$ mm, 6 mm, 16 mm and 40 mm). It allows a direct comparison with the fracture surfaces in Figs. 5.9 and 5.12 respectively.

Fig. 5.9 shows a fracture surface of the specimen with one central crack (a/b = 1.000) fractured with a medium loading rate, $v_K = 1.6 \times 10^3$ MPa$\sqrt{\text{m/s}}$. A flat tongue-like crack extension in the centre of specimen thickness (length approximately 4 mm) was observed at the beginning of crack extension. This flat area is bounded at both sides by shear lips. For an increasing $\Delta a$, the shear lip width increases while the width of the tongue decreases. The tongue has disappeared at $\Delta a = 4$ mm, and in the $\Delta a$-interval between 4 and 6.5 mm there is a region of fully slant fracture surface with shear lips across the full specimen thickness. However, from $\Delta a = 6.5$ mm onwards, the central flat portion of the fracture surface
appears again. The thickness of the shear lips now becomes slightly smaller for increasing \( \Delta a \) with a tendency to decrease until the edge of the specimen. The above variation of the shear lip width can also be observed in Fig.5.13. The variation of shear lip width \( f \) as a function of the crack extension is plotted in Fig.5.22.

At distances of \( \Delta a = 1 \text{ mm}, 6 \text{ mm} \) and \( 16 \text{ mm} \), the fracture surface was observed in detail in the scanning electron microscope. SEM micrographs are presented in Figs.5.16 to 5.18. The pictures were made at midthickness of the sheet. Fig.5.15 confirms a dimple type structure of the fracture in the region of the initial crack extension, which is typical for a slow stable crack extension with a coalescence of small microvoids. Fig.5.16 shows a fracture surface with many intergranular delamination facets created during fast unstable crack growth. The frequency of the intergranular delaminations slightly increased with an increasing distance from the unstable crack origin and with increasing crack speed. Fig.5.17 shows details of the fracture surface 16 mm far from the crack initiation site and about 10 mm from the end of the fully slant region where the crack growth became unstable.

Figs.5.11 to 5.13 show fracture surfaces of specimens fractured dynamically. The fracture surface of the specimen with \( a/b = 0.715 \) (Fig.5.10) differs from that in Fig.5.9. The tongue of the initial crack extension is not immediately completed by a fully slant region, as it occurred in Fig.5.9. In Fig.5.10, the width of the flat central band changes continuously. A relatively very short fully slant region appears at the crack extension \( \Delta a = 10 - 11 \text{ mm} \). Afterwards the thickness of shear lips slightly decreases up to end of specimen with a flat central band in between.

The SEM micrographs in Figs.5.18 - 5.20, again at midthickness of the fracture surface, were made on the same specimen at distances \( \Delta a = 1 \text{ mm}, 6 \text{ mm}, 16 \text{ mm} \). The pictures reveal again dimples in the slow stable crack growth area near the crack growth origin (Fig.5.18), and intergranular delamination facets in the region of fast unstable crack growth (Figs.5.19 and 5.20).

Fig.5.11 shows fracture surface of the specimen with \( a/b = 0.707 \) which was found to have the lowest fracture toughness. In this case the central flat band runs along the whole fracture surface, without being interrupted by a fully slant region. The thickness of the shear lips was
not enough to spread over the full specimen thickness for any crack extension.

The fracture surface of the specimen with \( a/b = 0.674 \) (Fig. 5.12) qualitatively revealed the same observations. No fully slant region was found on the fracture surface. However, the thickness of shear lips was larger and consequently the central flat part was narrower as compared to the previous specimen in Fig. 5.11. The specimen in Fig. 5.12 is characterized by \( a/b = 0.674 \) for which the critical stress for fracture of tips A imply a dynamical critical stress at tips B (see Fig. 5.3). Fracture of both tips of this specimen runs more or less immediately and the dynamic effects are reduced. At the edge of the specimen, the thickness of the flat and slant parts of the fracture surfaces were approximately the same as for all above specimens (see also Fig. 5.22). Fracture surfaces of several specimens are shown in Fig. 5.21 at a low magnification. The specimens were tilted by 45° for making the picture. The dark band in the middle of the specimen thickness correspond to the flat part of the fracture surface, while the slant parts of the fracture surface are more lit up, giving the white colour.

The shear-lip width of one quasi-statically and three dynamically fractured specimens is presented as a function of the crack extension in Fig. 5.22. It illustrates that a full slant fracture was obtained in two specimens \( (a/b = 1 \) and \( a/b = 0.715 \) during a short \( \Delta a \) interval. Those are the two specimens, which showed some stable crack extension before unstable crack extension occurred. They initially behaved as usual for a quasi-static type of loading. In the other two specimens \( (a/b = 0.707 \) and \( a/b = 0.674 \) the onset of crack extension (at \( F_A \)) was almost immediately followed \( (a/b = 0.707, \) see Fig. 5.1c) or directly followed \( (a/b = 0.674, \) see Fig. 5.1d) by crack instability, and some full slant fracture does no longer occur. In other words, a stable crack extension and a fast running crack give different fracture surfaces.
6. Discussion

The main observations of the analysis of the experimental results in the previous chapter are:

- The tests on the two-collinear-cracks specimen have shown that the residual strength can be sensitive to the loading rate at the crack tip, characterized by $dK/dt$. A reduction of the residual strength in the order of 10% was found. Although this is not very large from an engineering point of view, it is large enough to consider possible consequences for the residual strength of aircraft structures.

- The test on the two-collinear-cracks specimen have also shown that ligament failure does not necessarily imply a sufficiently high $dK/dt$ to cause the 10% drop of the residual strength. Ligament failure obviously increases the $K$-value of the outer crack tips quite abruptly. However, if ligament failure can still occur in a quasi-static way, in spite of the dynamic load on the specimen, and that also occurred in our test set up, there was no significant strength reduction.

- It should be pointed out that the above observations could be obtained by virtue of (1) fracture mechanics considerations on the two-collinear-cracks specimen, (2) COD measurements during slow and fast running cracks, and (3) meticulous fractographic observations.

- Apparently, a fast crack extension promotes a lower shear lip width with a flat rim between the two shear lips. If this occurs, the effective crack growth resistance will be lower. Such flat rims can be related to an increased plane strain influence, due to a higher yield stress. It is possible indeed that there is a dynamic effect on crack tip plastic deformation.

If we now return to the aircraft engineering significance of the above observations, it should be recalled first, that the tests were done on 2 mm 7075-T6 sheet material. A number of tests were also carried out on the same two-collinear-cracks specimen of 2024-T3. The results in Fig.5.23 show a good deal of scatter, but there are no indications of any strength reduction. The 2024-T3 alloy is more ductile than the 7075-T6 alloy. Ligament failure in the 2024-T3 specimen is preceded by significant plastic extension in the ligament. A high $dK/dt$ at the outer crack tips B can not be expected. The unstable crack extension occurred in the fully slant mode without a flat rim at midthickness. It appears that practical problems of a reduced dynamic fracture toughness can occur if crack extension is accompanied by
limited plastic deformation. It is not expected to be a problem for thick 7075-T6 components, because then plane strain prevails anyhow. The relevant fracture toughness property is $K_{Ic}$ and not $K_c$. However, for 7075-T6 sheet material, a high $dK/dt$ due to some dynamic effect on $\sigma_{0.2}$ could promote some plane strain influence. The present investigation is an exploratory investigation. Extensions to still larger $dK/dt$ values should be worthwhile as suggested by Fig.5.7. At the same time, $dK/dt$ in a structure is also depending on the type of structure. It is possible that there crack scenarios of MSD or WSFD, where critical situations can arise.

The relation between the loading rate and the plastic zone size, which yields the relation between crack speed and shear-lip width, can be used for analysis of the fracture surface of failed components. An example was available of an old fatigue test series [23] on full-scale wing panels with a 2 mm thick 7075-T6 Al-alloy skin. The skin was locally reinforced by a bonded finger plate. The photograph in Fig.5.24 shows the top of three fingers with a large fatigue crack nucleus (through crack) at the left finger tip. Very small part through fatigue crack nuclei are present at the middle finger (practically invisible) and a fairly large crack nucleus at the right finger tip. A sketch of the fracture surface is given in Fig. 5.25. Between the finger tips, the skin shows light parts (slant mode), and dark parts (flat tensile mode), compare to Fig.5.21. The failure sequence can now be reconstructed as schematically indicated in Fig.5.25. The panel failure started from the through crack at the left finger tip with some stable crack growth (tongues), followed by fast crack growth to the middle finger. Near the middle finger the crack growth rate was slowed down, due to the presence of the still unbroken stiffener flange. After stiffener failure a fast crack growth occurred again, which simply linked up the fatigue crack at the right finger tip. Although the picture in Fig.5.25 has a qualitative meaning only, it can be most helpful to reconstruct the cracking sequence, which during panel failure can not be observed at all.
7. Conclusions

An exploratory investigation was carried out on the effect of dynamic loading on the residual strength and fracture toughness of 7075-T6 aluminium alloy sheet material. Tests were carried out on long sheet specimens, provided with two collinear cracks. First the ligament between the two cracks fails, which implies that they are linked up to a single crack. Afterwards, the single crack leads to overall failure of the specimen. During the link up process a sudden increase of the outer crack tip stress severity occurs. Comparative tests were carried out on the same specimens with a single crack with the same crack length as the two cracks together after linking up. The following conclusions were obtained:

1. The specimen with two collinear cracks was a useful specimen to study dynamic failure of sheet material. The fracture mechanics analysis, including initial stable crack extension was essential for planning the test program (i.e. to select informative crack length dimensions). COD-measurements with a photo-cell test set up during the dynamic tests allowed the determination of \( a_{ef} \) during dynamic crack growth.

2. Due to ligament failure under a high loading rate on the specimen, the loading rate in the outer crack tip zones could be amplified to \( 2 \times 10^4 \text{ MPa}/\text{m/s} \). At such a high loading rate the residual strength was decreased by about 10% as compared to the quasi-static result. The engineering fracture toughness decreased by 8% and the effective fracture toughness by 12%.

3. Fractographic evidence indicates that a high \( dK/dt \) has some effect on the shear lips, i.e. shear lips with a flat rim (tensile mode) at midthickness are observed for fast running cracks, whereas quasi-stable crack extension occurs in the fully slant mode. It appears that high loading rates promote some plane-strain influence, associated with an increased yield stress, due to the high plastic strain rate in the crack tip zone.

4. The observation, mentioned in the previous conclusion, can be useful for fractographic analysis of failures in aircraft sheet components of 7075-T6 sheet material, with respect to indicate either slow stable crack extension or a fast running fracture.

5. The flat rim at midthickness was not observed for unstable crack extension in 2024-T3 sheet material.

6. Dynamic crack growth resistance effects should be considered for the residual strength of aircraft structures built up from 7075-T6 sheet material.
References


Appendix A. Quasi-static stable crack extension in thin sheets

Vlasveld and Schijve [21] developed a model to describe the growth of tongue-like crack tips during fatigue overloads in high strength aluminium alloys. A tongue-shaped crack extension of a fatigue crack in the tensile mode occurs in the centre of the specimen thickness by tunnelling of the crack tip between two unfractured ligaments at the specimen surface, see Fig.A1a. The tip of the tongue in the centre is supposed to be in plane strain with $K_{ic}$ as the applicable stress intensity criterion for crack extension.

The unfractured ligaments, are supposed to be in plane stress. Similar to the Dugdale approach, Vlasveld and Schijve assumed that the ligaments are carrying a stress $\sigma_{0.2}$, and this reduces the stress intensity along the (partly) imaginary crack front. The stress intensity factor necessary for crack extension $\Delta a$ is then higher than $K_{ic}$ by an amount $K_t$ which is a function of $\Delta a$:

$$K_{\Delta a} = K_{ic} + K_t(\Delta a)$$ (A1)

where $K_t$ is stress intensity factor corresponding to the effective ligament stress $\sigma_t$ acting on the full thickness $t$ [20]:

$$K_t = \int_{a}^{a+\Delta a} \frac{\sigma_t(x) \, dx}{\sqrt{\pi (a+\Delta a)}} \left( \sqrt{\frac{(a+\Delta a)+x}{(a+\Delta a)-x}} + \sqrt{\frac{(a+\Delta a)-x}{(a+\Delta a)+x}} \right)$$ (A2)

The function $\sigma_t(x)$ is given by the ligament width $u_t(x)$:

$$\sigma_t(x) = \frac{2 \, u_t(x)}{t} \, \sigma_{0.2}$$ (A3)

The ligament width for $x = a + \Delta a$ was supposed to be equal to the plane stress plastic zone size:
In the thinner specimens the ligament at the crack tip will cover the full width because

\[ r_p > \frac{t}{2} \]  \hspace{1cm} (A5)

even at crack initiation. Functions obtained in [21] should be rederived. Fig. A1b shows successive steps of the crack front. The two ligaments always meet in the centre of a specimen, and the shape of the tongue is approximated by a triangle, unlike the shape in the thicker specimen, which was approximated by a trapezium with a flat crack front (Fig. A1a).

The ligament width in a very thin specimen at a crack increment \( \Delta a \) is described by the function:

\[ u_1(x) = \frac{t/2}{\Delta a} (x - a) \]  \hspace{1cm} (A6)

With substitution (A.6) and (A.3) and integration of (A2), Eq.(A1) can be written as:

\[ K = K_{1c} + \frac{2\sigma_{0.2}}{\sqrt{\pi(a+\Delta a)}} \left[ \sqrt{1 + \frac{2a}{\Delta a}} - \frac{a}{\Delta a} \arcsin \left( \frac{1+\Delta a}{a} \right) \right] \]  \hspace{1cm} (A7)

The equation has been plotted in Fig. A2.

For \( K_{\Delta a} = K_c = 71.5 \text{MPa}\text{m}^{1/2} \) solution of Eq. (A7), for \( a = 30 \text{mm} \), \( \sigma_{0.2} = 530 \text{MPa} \) and \( K_{1c} = 30 \text{MPa}\text{m} \), leads to critical stable crack extension \( \Delta a_c = 5.3 \text{mm} \). This value is in very good agreement with the critical quasi-static stable crack growth increment found at fracture surface analysis \( \Delta a_c = 4 - 6.5 \text{mm} \) (see Chapter 5.3).

The function \( K_{\Delta a} (\Delta a) \) can be approximated very accurately (within 1\%) by a power law:

\[ K_{\Delta a} = K_{1c} + 17.9 \sqrt{\Delta a} \]  \hspace{1cm} (A8)

(K in MPa\text{m}, \( \Delta a \) in mm).
Fig. A1a Tongue in plate material

Fig. A1b Tongue in thin sheet material

Fig. A2 Stress intensity factor necessary for crack extension $\Delta a$. 

$$K_{IC} = 30 \text{ MPa$\sqrt{m}$}$$

$$K_{C} = 71.5 \text{ MPa$\sqrt{m}$}$$

$$K_{\Delta a} = K_{IC} + K_{I}$$

$$K_{I} = 17.9 \Delta a^{0.5}$$
Appendix B. Geometrical correction factors

1. A correction factor for a central crack in a sheet of finite width is given in [20] as:

\[ f_w(a,w) = [1 - 0.1(a/w)^2 + 0.96(a/w)^4] \sqrt{\sec(\pi a/w)} \]  

(B1)

2. Correction factors for two equal collinear cracks in a sheet of finite width for tips A and B (Fig. 1.2) are obtained by compounding [23] of two ancillary configurations:

\[ f_A(a,b,w) = f_A(a/b) \cdot f_{AW}(a,b,w) \]  

(B2)

\[ f_B(a,b,w) = f_B(a/b) \cdot f_{BW}(a,b,w) \]  

(B3)

where \( f_A(a/b) \) and \( f_B(a/b) \) are correction factors for tips A and B of two equal collinear cracks in an infinite plate; \( f_{AW}(a,b,w) \) and \( f_{BW}(a,b,w) \) are correction factors for tips A and B of one eccentric crack in a sheet of finite width.

Correction factors for two equal collinear cracks are given in [20] as

\[ f_A(a/b) = \frac{1}{k} \sqrt{\frac{1 + a/b}{a \cdot a/b} \left( \frac{E(k)}{K(k)} - \alpha^2 \right)} \]  

(B4)

\[ f_B(a/b) = \frac{1}{k} \sqrt{\frac{1 + a/b}{a/b} \left( 1 - \frac{E(k)}{K(k)} \right)} \]  

(B5)

where \( \alpha = (1-a/b)/(1+a/b) \). \( K(k) \) and \( E(k) \) are complete elliptic integrals of the first and second kind respectively with \( k = \sqrt{(1-\alpha^2)} \).

Correction factors for eccentric crack are given in [24] and for the notation of two equal collinear cracks (Fig. 1.2) can be written as:

\[ f_B(a,b,w) = \sqrt{\sec(\pi \left( \frac{2.65a}{6(w/2-b)} + \frac{0.35a}{w/2+b} \right))} \]  

(B6)

\[ f_A(a,b,w) = \sqrt{\sec(\pi \left( \frac{1.45a}{6(w/2-b)} + \frac{1.55a}{w/2+b} \right))} \]  

(B7)
Table I  Heat treatment and chemical composition of 7075-T6 aluminium alloy sheet.

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<th>chemical composition (weight %)</th>
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<td>5.1-6.1</td>
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artificial aging: 24 hrs at 121°C

Table II  Mechanical properties of 2 mm thick 7075-T6 aluminium alloy sheet in longitudinal rolling direction.

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<th>$\sigma_{0.2}$ (MPa)</th>
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<th>$\varepsilon_{fr}$ (%)</th>
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<th>$E$ (MPa)</th>
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Table III  Residual strength tests: Dimensions of two collinear cracks, strength of tips A-A ($\sigma_A$), strength of the specimen ($\sigma_c$) and engineering fracture toughness ($K_{ic}$).

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<th>$2a$ (mm)</th>
<th>$2b$ (mm)</th>
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<th>$F_c$ (kN)</th>
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<td>(229)</td>
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Table IV  Effective crack length and effective stress intensity factor at instability of tips A-A, at full crack opening and at instability of the specimen (effective fracture toughness). Fracture time and effective load rate.

<table>
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<tr>
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<th>a_{p,eff} (mm)</th>
<th>a_{full,eff} (mm)</th>
<th>a_{c,eff} (mm)</th>
<th>K_{B,eff} (MPa/\sqrt{m})</th>
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Fig. 1.1 Long sheet specimen with two equal collinear cracks.

Fig. 1.2 Configuration of the two collinear cracks.
Fig. 3.1 Normalized stress intensity factors of the two collinear cracks at the moment that $K_A = K_c$ ($a_{full} = 60$ mm). Stable crack growth is not taken into account.

Fig. 3.2 Normalized stress intensity factors of the two collinear cracks at the moment that $K_A = K_c$ ($a_{full} = 60$ mm). Stable crack growth has been taken into account.
Fig. 4.1  Schematic arrangement of the COD-meter device. Through the saw-cut slot passing light is proportional to opening of the slot.

Fig. 4.2  Set-up of the COD-meter photo cell.
COD$_x$ (mm) = 1.04 $\times$ $\Delta$U$_{COD}$ (V)

Fig.4.3  Calibration curve of the COD-meter.
Load and COD signal ($U_{\text{COD}}$) as stored during residual strength tests.

$F_A =$ critical load for A-tip fracture, $F_C =$ critical load for specimen fracture.

(a) one crack: $a/b = 1.000$, $2a_{\text{full}} = 60.4$ mm

(b) two cracks: $a/b = 0.775$, $2a_{\text{full}} = 60.0$ mm

(c) two cracks: $a/b = 0.707$, $2a_{\text{full}} = 60.5$ mm

(d) two cracks: $a/b = 0.674$, $2a_{\text{full}} = 60.1$ mm

(e) two cracks: $a/b = 0.628$, $2a_{\text{full}} = 60.5$ mm
Fig. 5.1 (b) two cracks: $a/b = 0.775$, $2a_{\text{full}} = 60.0$ mm

Fig. 5.1 (c) two cracks: $a/b = 0.707$, $2a_{\text{full}} = 60.5$ mm
Fig. 5.1 (d) two cracks: $a/b = 0.674$, $2a_{\text{full}} = 60.1$ mm

Fig. 5.1 (e) two cracks: $a/b = 0.628$, $2a_{\text{full}} = 60.5$ mm
Fig. 5.2 Load versus COD curves.

(a) one crack: $a/b = 1.000$, $2a_{\text{full}} = 60.4$ mm

(b) two cracks: $a/b = 0.775$, $2a_{\text{full}} = 60.0$ mm

(c) two cracks: $a/b = 0.707$, $2a_{\text{full}} = 60.5$ mm

(d) two cracks: $a/b = 0.674$, $2a_{\text{full}} = 60.1$ mm

(e) two cracks: $a/b = 0.628$, $2a_{\text{full}} = 60.5$ mm
Fig. 5.2  
(b) two cracks: \(a/b = 0.775\), \(2a_{\text{full}} = 60.0\) mm

Fig. 5.2  
(c) two cracks: \(a/b = 0.707\), \(2a_{\text{full}} = 60.5\) mm
Fig. 5.2 (d) two cracks: $a/b = 0.674$, $2a_{full} = 60.1$ mm

Fig. 5.2 (e) two cracks: $a/b = 0.628$, $2a_{full} = 60.5$ mm
Fig. 5.3  Critical stress at A-tip instability (σ_A) and critical stress at specimen instability (σ_c) as a function of the ratio a/b.

Fig. 5.4  Dynamic effect of the geometrical configuration of two equal collinear cracks (ratio a/b) on the engineering fracture toughness (K_c,0) of specimens loaded with higher loading rates.
Fit of the constant-compliance straight line on the load versus COD curve for two different effective crack lengths.

Dynamic effect of the geometrical configuration of two equal collinear cracks (ratio \(a/b\)) on the effective fracture toughness \((K_{e,\text{eff}})\) of specimens loaded with higher loading rates.
Fig. 5.7  Dynamic effect of higher loading rates ($v_{K,\text{eff}}$) on the effective fracture toughness ($K_{c,\text{eff}}$).

Fig. 5.8  Relation between effective fracture toughness ($K_{c,\text{eff}}$) and effective critical crack growth increment ($\Delta a_{c,\text{eff}}$) at higher rates of loading.
Fracture surface of the specimen with one central crack, (a/b = 1.000, loading rate $v_{K,eff} = 1.6 \times 10^3 \text{ MPam}^{1/2} \text{s}^{-1}$). SEM.

Fig. 5.9
Crack growth increment, $\Delta a$ (mm)

Fig. 5.9 Fracture surface of the specimen with one central crack, ($a/b = 1.000$, loading rate $v_{K,\text{eff}} = 1.6 \times 10^3 \text{ MPam}^{1/2}\text{s}^{-1}$). SEM. (Cont.)

To be continued
Fracture surface of the specimen with one central crack, \(a/b = 1.000\), loading rate \(v_{K,\text{eff}} = 1.6 \times 10^3 \text{ MPam}^{1/2} \text{s}^{-1}\). SEM. (Cont.)
Fig. 5.10 Fracture surface of the specimen with two equal collinear cracks, ($a/b = 0.715$, loading rate $v_{K,eff} = 9.4 \times 10^3$ MPa$m^{1/2}$s$^{-1}$). SEM.
Fracture surface of the specimen with two equal collinear cracks, \(a/b = 0.715\), loading rate \(v_{K,eff} = 9.4 \times 10^3\) MPam\(^{1/2}\cdot\text{s}^{-1}\). SEM. (Cont.)
Fracture surface of the specimen with two equal collinear cracks, \( \frac{a}{b} = 0.715 \), loading rate \( v_{K,\text{eff}} = 9.4 \times 10^3 \text{ MPa}^{1/2}\text{s}^{-1} \). SEM. (Cont.)
Fracture surface of the specimen with two equal collinear cracks, (a/b = 0.707, loading rate $v_{k,\text{eff}} = 15.9 \times 10^3$ MPam$^{1/2}$s$^{-1}$). SEM.
Fracture surface of the specimen with two equal collinear cracks, (a/b = 0.707, loading rate $v_{K,eff} = 15.9\times10^3$ MPam$^{1/2}$s$^{-1}$). SEM. (Cont.)
Fig. 5.11 Fracture surface of the specimen with two equal collinear cracks, \(a/b = 0.707\), loading rate \(v_{K,eff} = 15.9 \times 10^3\) MPam\(^{1/2}\)s\(^{-1}\). SEM. (Cont.)

Crack growth increment, \(\Delta a\) (mm)
Fracture surface of the specimen with two equal collinear cracks, \(a/b = 0.674\), loading rate \(v_{K,\text{eff}} = 27.0 \times 10^3 \text{MPa m}^{1/2} \text{s}^{-1}\). SEM.

To be continued
Fracture surface of the specimen with two equal collinear cracks, \(a/b = 0.674\), loading rate \(v_{K,\text{eff}} = 27.0 \times 10^3\) MPam\(^{1/2}\text{s}^{-1}\). SEM. (Cont.)
Fracture surface of the specimen with two equal collinear cracks, \( a/b = 0.674 \), loading rate \( v_{K,eff} = 27.0 \times 10^3 \) MPam\(^{1/2}\)s\(^{-1}\). SEM. (Cont.)
Fig. 5.13  Perpendicular cuts of the fracture surface of the specimen from Fig. 5.10. Polishing, light microscope.
Fig. 5.14  Perpendicular cuts of the fracture surface of the specimen from Fig. 5.13.
Polishing, light microscope.
Fig. 5.15 SEM micrographs of the fracture surface of the specimen from Fig. 5.10. 
\( \Delta a = 1 \text{ mm} \), middle of the surface.
FIG. 5.16 SEM micrographs of the fracture surface of the specimen from Fig. 5.10. 
$\Delta a = 6\,\text{mm}$, middle of the surface.
SEM micrographs of the fracture surface of the specimen from Fig. 5.10. 
\( \Delta a = 16 \text{ mm} \), middle of the surface.

\[ a/b = 1.000 \]
Fig. 5.18 SEM micrographs of the fracture surface of the specimen from Fig. 5.11. 
\( \Delta a = 1 \text{ mm} \), middle of the surface.
Fig. 5.19 SEM micrographs of the fracture surface of the specimen from Fig. 5.11. 
Δa = 6 mm, middle of the surface.
Fig. 5.20 SEM micrographs of the fracture surface of the specimen from Fig. 5.11. 
\( \Delta a = 16 \text{ mm}, \text{middle of the surface.} \)
Fig. 5.21  Fracture surfaces of the specimens tested. Light microscope, tilt 45°.
Shear lip thickness ($\ell$) as a function of crack extension ($\Delta a$).

Dynamic effect of the geometrical configuration of two equal collinear cracks (ratio $a/b$) on the engineering fracture toughness of 2 mm 2024-T3 Al-alloy sheet loaded with higher loading rates.
Fig. 5.24
Fracture of the panel made from 2 mm thick 7075-T6 Al-alloy sheet.
Fig. 5.25 Qualitative reconstruction of a failure history of the panel from Fig. 5.28.
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<tr>
<td>F. Motallebi, 'Prediction of Mean Flow Data for Adiabatic 2-D Compressible Turbulent Boundary Layers'</td>
<td>F. Motallebi</td>
<td>1997</td>
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<td>B.W. van Oudheusden, 'Investigation of Large-Amplitude 1-DOF Rotational Galloping'</td>
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Dynamic tests were carried out on long sheet specimens with two collinear cracks.