MTS Adhesives Project 2: Failure Modes and Criteria

Report No. 1

Review of Adhesive Bond Failure Criteria

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Edited by JC McCarthy□

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+ The University of Surrey
• Imperial College
□ AEA Technology
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- REVIEW OF ADHESIVE BOND FAILURE CRITERIA

### ISSUE RECORD

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MTS ADHESIVES PROJECT 2: FAILURE MODES AND CRITERIA

Foreword

Many UK manufacturers are aware of the merits of adhesives in certain critical roles. However the range of applications of adhesives is still limited largely due to the lack of consistent test methods and validated test data which the engineer needs in order to specify adhesives for a given application. In a recent survey the Centre for Adhesive Technology was commissioned by the DTI to establish specific areas where validated test methods could improve confidence in predicting joint life. The survey identified measurement methods for use in design, environmental durability and process control as priority areas and five projects were finally selected by the DTI for support through the Measurements Technology and Standards (MTS) budget. The projects started in December 1992 and are 100% funded by the DTI at the level of £5.4M over three years.

The survey identified the need to understand adhesive joint failure modes and the development of more robust, validated failure criteria as being critical to the development of confidence in adhesive bonding technology. This requirement forms the basis of MTS Adhesives Project 2 which is being carried out through a collaboration of AEA Technology, Surrey University and Imperial College.

The project is addressing the issue of failure criteria through initially an extensive study of joint fracture. This forms the project’s first task and it will give greater understanding to the mechanisms by which adhesive failure begins and propagates through the joint. The task makes extensive use of Scanning Electron Microscopy and Laser Moire Interferometry, both techniques being used in-situ on joints as they fail where possible and applicable. The project’s other two major tasks are to investigate and develop new and existing failure criteria, and to investigate and develop tests for the measurement data needed to make the criteria work. These tasks run in parallel through the second and third years of the project. All the major loading modes will be addressed in the project - static, fatigue, creep and impact loadings. The criteria for assessment of the failure criteria are that they should be accurate whilst being as easy to apply as possible and those for the tests are that they should be as accurate as necessary to give good predictions of failure whilst being easy to use and using equipment that is as cheap as possible.
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Glossary

\( \gamma \)  shear strain
\( \Gamma \)  correction factor
\( \Delta \)  change in
\( \delta \)  crack opening displacement
\( \varepsilon \)  normal strain
\( \lambda \)  strength of singularity
\( \sigma \)  normal stress
\( \tau \)  shear stress
\( \Phi \)  dimensionless geometry factor
\( \chi \)  transient strain
\( \psi \)  time
\( \omega \)  natural frequency
\( \Omega \)  correction factor
\( a \)  crack length
\( \Lambda \)  a constant
\( a' \)  crack growth rate
ABAQUS  a proprietary finite element code
ANSYS  a proprietary finite element code
ASTM  American Society of Testing and Measurement
B  a constant
BS  British Standard
c  crack length
C  creep compliance
cfrp  carbon fibre reinforced plastic composite
CLS  cracked lap shear
comp shear  composite shear
\( \cos \)  cosine
CTBN  carboxyl-terminated butadiene-nitrile
CTOD  crack tip opening displacement
d strap  double strap
D  thickness
d  displacement
da/dN  rate of crack growth with cycles
DCB  double cantilever beam
dlj  double lap joint
E  tensile modulus
enf  edge notch flexure
ESIS  European Structural Integrity Society
F  loss function
f()  function of
FCGR  fatigue crack growth rate (also known as FCPR)
FCPR  fatigue crack propagation rate (also known as FCGR)
FEA  finite element analysis
g  energy dissipated in viscoelastic and plastic deformation
gfrp  glass reinforced plastic composite
G  energy release rate
h  \( \frac{1}{2} \) thickness of substrate
I  Mode I loading ie peel or crack opening
Mode II loading i.e. in-plane shear

International Standards Organisation

J integral

computer program for the analysis of joints based on Hart-Smith’s work

stress intensity factor

function

length

linear elastic fracture mechanics

natural logarithm

function

number of cycles at given load

number of cycles to failure

a proprietary finite element code

load

stress intensity factor

distance from singularity

gas constant

stress/number of cycles

single edge notch

single edge notch three point beam

sine

single lap joint

temperature

time

thick adherend shear test

tapered double cantilever beam

tetraethylenepentamine

strain energy

viscoplasticity based on over-stress

a proprietary finite element code

width

Poisson’s ratio

summation

intrinsic or initial value

critical

effective

failure

mode I i.e. peel or opening

initiation

mode II i.e. in-plane shear

joint

maximum

total

threshold

at yield

SI units are assumed.
1. Introduction

This report was compiled as part of the activities undertaken within Task 3 "Development of Failure Criteria" and forms the first deliverable from that task. The report is a review of proposed failure criteria for adhesive joints, covering all types of joint geometry and the four main loading conditions: static; fatigue; long term static (creep); impact.

As such the criteria differ in absolute sense between the load types:

- criteria for static and impact loadings attempt to predict the load the joint will carry;
- criteria for fatigue loading attempt to predict the lifetime of the joint under an applied mean and cyclicly varying load of some frequency in terms of cycles to failure;
- criteria for creep loading attempt to predict the lifetime of the joint under some constant load in terms of time to failure.

The unifying purpose however between the criteria is that they should allow engineers to assess the fitness for purpose of their joint designs.

The review consists of the following:

- a brief discussion of the work that has been carried out on a particular approach for a type of loading condition;
- conclusions on the particular approach and its likelihood of success as a unifying criterion;
- recommendations on criteria that should be investigated for a particular loading condition in the programme.

The review is extensive (50 pages plus figures) and is not aimed as a primer in adhesive bond failure criteria but as a summary for an experienced researcher of the current state of research. As such the review will be used to form the work plan for the later activities on Task 3 which, together with the results from Task 1 of the project "Detailed Studies of Joint Failure", will investigate the more promising failure criteria further with a view to recommending proven criteria for design.

The reader should also be aware of a review of test methods for the measurement of fracture properties produced under Task 2 of the project which may viewed as a complement to this review. The Task 2 review presents the many existing tests used to measure the adhesive fracture properties required for the failure criteria discussed herein.
In reading this review it is worth remembering that predicting failure in single material systems, i.e., monolithic metals or polymers, is difficult and in many cases the best definitive failure criterion is still debated. Multimaterial systems such as composites add additional complexity and a plethora of failure criteria. However, this has not prevented these materials from being used extensively through the use of robust design guidelines.

Whilst a casual consideration of adhesively bonded joints might suggest that predicting the failure of such systems should be easy, more considered examination reveals the intricate stress distributions and complex material behaviour of the adhesives which are themselves 'composites'. Hence, the large number of often conflicting results from researchers should be of no surprise given the different nature of their adhesives, joint geometries, loads, etc.

It is the purpose of MTS Adhesives Project 2 to bring some rational to this situation and by a combination of review and test to determine which criteria can be relied upon to operate successfully in the design process.
2. Short Term Static Loading Failure Criteria

2.1 Introduction

Numerous failure criteria for adhesive joints have been proposed and used with varying success. The majority of the early failure criteria were based on critical values of stress or strain for unflawed joints and linear elastic fracture mechanics for cracked joints. This section aims to review the various forms of failure criteria that have been proposed for adhesive joints and, where possible, to assess their applicability to typical joint configurations. The remainder of this section has been subdivided into areas covering the various type of failure criteria:

- maximum stress/strain
- stress/strain and a distance
- limit state analysis
- fracture mechanics
- bi-material singularities
- damage modelling.

A large number of researchers have proposed and used various failure criteria. Their work is reviewed in subsequent sections but it was felt useful to summarise it and this is presented in table 1.

2.2 Maximum stress/strain

Maximum stress/strain failure criteria are the most intuitive starting point for joint strength predictions. Such a form of failure criterion assumes that the joint will fail when a critical value of stress/strain is reached at any point within the joint.

2.2.1 Maximum shear stress criterion

A maximum shear stress failure criterion was proposed and used by [Greenwood (1969)] to predict the strength of single lap-joints (made with 12mm thick tool steel substrates and AY103/951 adhesive system) under a range of loading conditions. In this work a [Goland and Reissner (1944)] closed form analysis was used and the maximum shear stresses were found to occur at about 45° across the adhesive layer. Using the maximum shear stress obtained from a tensile test of the bulk adhesive the predicted strength was about 14% too low. Unfortunately values of the maximum principal stresses (which might be more appropriate for the adhesive used) were not determined. [Hart-Smith (1973)] also included a maximum shear stress criterion when he proposed that one of the categories of joint failure was local shear failure in the plane of the adhesive. However he also states that this failure mode is "extremely rare in structural practice".

2.2.2 Maximum peel stress criterion

[Hart-Smith (1973)] also included local peel failure in his modes of failure for a single lap joint. This type of criterion has also been used more recently by [Crocombe et al (1985)]. In this latter work results from a simple linear closed form analysis (which modelled only the peel stresses in the adhesive), were used for strength predictions of variously loaded "T" joints made by bonding 3mm steel with AY103/956. A maximum adhesive tensile strength of 69MPa was obtained from bulk tensile test data. Using this as the critical value of peel stress gave strength...
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<tr>
<th>Criterion</th>
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<th>Overlap thickness</th>
<th>Substrate thickness</th>
<th>Adhesive thickness</th>
<th>Substrate</th>
<th>Adhesive</th>
<th>Critical value</th>
<th>Source of value</th>
<th>Approx. error</th>
<th>Main parameters</th>
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<tr>
<td>Shear stress</td>
<td>Greenwood (1969)</td>
<td>slit</td>
<td>75 **</td>
<td>12.5</td>
<td>0.1</td>
<td>tool steel</td>
<td>AY103/651</td>
<td>22MPa</td>
<td>tensile test</td>
<td>16%</td>
<td>Mode of loading</td>
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<td>Peel stress</td>
<td>Crocombe (1989)</td>
<td>T joint</td>
<td>20 **</td>
<td>2.2</td>
<td>0.22, 0.4</td>
<td>mild steel</td>
<td>AY103/656</td>
<td>65MPa</td>
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<td>1.6</td>
<td>0.125</td>
<td>A14500, B11000</td>
<td>MY1233/5225</td>
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<td>Chal (1993)</td>
<td>slj</td>
<td>20 **</td>
<td>2 and 6.5</td>
<td>0.02, 1.0</td>
<td>steel</td>
<td>IPC9623</td>
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<td>tension test</td>
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<td>Le (1992)</td>
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<td>20 **</td>
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<td>0.125</td>
<td>(430, 220, 110MPa)</td>
<td>ESP105T, cbn epoxy</td>
<td>84, 75%</td>
<td>bulk tensile</td>
<td>5%, 11%</td>
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<td>slj</td>
<td>25 **</td>
<td>4.75/3</td>
<td>0.25</td>
<td>(450MPa²)</td>
<td>E27, ESP105T</td>
<td>47.74 MPa</td>
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<td>Plastic energy density</td>
<td>Adams (1987)</td>
<td>slit</td>
<td>25 **</td>
<td>3.2</td>
<td>0.25</td>
<td>(A34, 309MPa)</td>
<td>cbn epoxy</td>
<td>7.3±3, 3±3</td>
<td>peel test</td>
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<td>Zhao (1989)</td>
<td>slit</td>
<td>25 **</td>
<td>0.25</td>
<td></td>
<td>(304MPa)</td>
<td>cbn epoxy</td>
<td>7.3±3, 3±3</td>
<td>bulk tensile</td>
<td>-8%, 4%, 3%</td>
<td>End geometries</td>
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<td>Bigwood (1989) +</td>
<td>slit, comp shear</td>
<td>25 **</td>
<td>4.75/3</td>
<td>0.25</td>
<td>(450MPa²)</td>
<td>ESP105T</td>
<td>4%</td>
<td>bulk tensile</td>
<td>&lt;6%</td>
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<td>Stress/strain and distance</td>
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<tr>
<td>Shear stress</td>
<td>John (1991)</td>
<td>dlj</td>
<td>10-100</td>
<td>2</td>
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<td>composite</td>
<td>Redux410, Hyso9321</td>
<td>38/0.1*oil</td>
<td>test</td>
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<td>Average principal stress</td>
<td>Zhao (1991)</td>
<td>slit</td>
<td>25 **</td>
<td>9.9</td>
<td>0.2%</td>
<td>(924MPa)</td>
<td>MY1233/5225</td>
<td>47MPa/adhesive</td>
<td>reduced bulk tensile</td>
<td>10%</td>
<td>Corner rounding</td>
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<td>Principal stress over zone</td>
<td>Clarke (1991)</td>
<td>dlj, d strap, T peel</td>
<td>10,5,20</td>
<td>8,2</td>
<td>0.2</td>
<td>(160, 240, 100MPa)</td>
<td>2 toughened epoxies</td>
<td>67MPa+*mm²</td>
<td>70MPa/0.68mm</td>
<td>bulk tests and dlj</td>
<td>&lt;15%</td>
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<td>Peel and equivalent stress</td>
<td>Crocombe (1994)</td>
<td>dcb</td>
<td>25 **</td>
<td>12.5</td>
<td>2</td>
<td>(450MPa)</td>
<td>£27</td>
<td>20MPa/0.3mm, 40MPa/0.01, 0.02mm</td>
<td>optimised fit</td>
<td>&lt;5%</td>
<td>mode of loading, crack length</td>
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<tr>
<td>Peel stress</td>
<td>Kinloch (1980++)</td>
<td>bulk and joint idcb</td>
<td>25 **</td>
<td>5</td>
<td>0,25</td>
<td>(304MPa)</td>
<td>cbn-epoxy</td>
<td>250-500MPa/10-0.1 microns</td>
<td>best fit to data</td>
<td>&lt;7%</td>
<td>Temperature and rate</td>
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<td>Ax plastic energy density</td>
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<td>25 **</td>
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<td>10%</td>
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<td>djl, comp shear</td>
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<td>0.05-0.5</td>
<td>(305, 450, 450MPa)</td>
<td>EA9309, REDUX908a, £27</td>
<td>bulk tensile tests</td>
<td>6%, 3%, 14%</td>
<td>ta, ts</td>
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**Table 1.** A Summary of the work that has been carried out investigating failure criteria joints subjected to short term static loading.
<table>
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<th>Fracture mechanism</th>
<th>Malyehev (1965)</th>
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<td>Molville (1979)</td>
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<td>Gc and inherent flaw size</td>
<td>Anderson (1988)</td>
<td>button, blister, db, sij, end, lateral, and peel</td>
<td>120</td>
<td>2.54-6.35</td>
<td>steel/concrete</td>
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<td>Narmco 5208</td>
<td>Narmco (ex BASF?)</td>
<td>Untoughened epoxide matrix resin</td>
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<td>BP Speciality Chemicals</td>
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<td>American Cyanamid</td>
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predictions to within 6% for two configurations but yielded errors of about 90% on the only configuration where substrate yielding was noted. Clearly this criterion is not universally applicable.

2.2.3 Peak maximum principal stress criterion

A peak maximum principal stress failure criterion has been used extensively by Adams and co-workers. An example of the application of this form of criterion are [Harris et al (1984)], predicting the strength of single lap joints to about 10% accuracy using elastoplastic finite element analysis. Aluminium substrates, of differing strengths were bonded using four different adhesives. It was shown that for two of the adhesives a critical stress criterion applied. This was not necessarily related to the ductility exhibited by the adhesive. A summary of measured and predicted strengths is shown in Figure 1. Only one adhesive thickness and mode of loading have been considered and the value of the maximum stresses are only an artefact of the finite element mesh used. This latter aspect noted in a later paper [Adams and Harris (1987)] and is discussed in more detail in the section on plastic energy density.

2.2.4 Maximum Von Mises equivalent stress criterion

Another alternative maximum stress failure criterion has been based on the peak value of the von Mises (equivalent) stress. This was used by [Ikegami (1989)] to predict the strength of one component of composite to metal scarf joints (namely adhesive failure) from elastic finite element analysis. Other possible modes of failure were in the substrates and at the interface. The von Mises stress provides an equivalent adhesive stress that can be related to the uniaxial yield stress. However the von Mises yield criterion neglects the hydrostatic component of stress which significantly affects the yield and deformation behaviour of polymers. Insufficient detail is give to enable a full evaluation of this criterion.

2.2.5 Maximum shear or peel strain criterion

The categories for failure in single lap joints proposed by [Hart-Smith (1973)] include shear and peel failure in the adhesive as mentioned previously. For ductile adhesives these are best expressed in terms of the adhesive strains. This has also been used by [Lee et al (1992)] who proposed a failure model for tubular lap joints in torsion which incorporated cohesive failure at low adhesive thicknesses and interfacial failure at higher adhesive thicknesses. The former was governed by a maximum strain criterion while the latter a maximum "reduced" stress criterion. It is not clear how to determine when a particular criterion should be applied. [Chai (1993)] has undertaken an experimental investigation involving testing cracked edge notch flexure specimens and measuring the strain field in the adhesive using high magnification video images. He shows that the critical shear strain appears to decrease with increasing adhesive thickness but that there is close correlation between the crack tip shear strain found in these tests and those measured in a napkin ring test. This is illustrated in Figure 2. However this implies that there is not a single material constant and thus its use is rather restrictive. Additionally these strains are somehow "averaged" as the crack tip strain is infinite and thus it is not immediately clear how these values might be used in an analysis where point values of strain are found. It has also been shown that this can be interpreted in terms of a critical fracture energy, once again the value of this varies with adhesive thickness.
2.2.6 Peak maximum principal strain criterion

The work of [Harris and Adams (1984)] has been discussed in a section above. They showed that for toughened adhesives the maximum principal stress was not as appropriate as maximum principal strain. Applying this criterion, in conjunction with an elastoplastic finite element analysis, reasonable strength predictions were obtained for single lap-joints, see Figure 1. The limitations discussed earlier, principally of the maximum strain being an artefact of the mesh size, still apply.

2.2.7 Maximum effective uniaxial plastic strain criterion

[Crocombe and Adams (1982)] used a maximum effective uniaxial plastic strain for predicting failure in peel joints using large displacement elastoplastic finite element analysis. They in fact point out the dependency of this strain on the mesh size and state that it is really strain at a specific distance. To obtain reasonable failure predictions it has been necessary to use different critical strains for different substrates bonded with the same adhesive. This has been attributed to differences in plastic zone sizes but is rather restrictive.

2.2.8 Maximum plastic energy density criterion

An alternative to the use of a strain based criterion for ductile adhesives is to use an energy density based criterion. [Adams and Harris (1987)] used this approach for the failure prediction of single lap-joints with varying end geometry, based on a plastic energy density criterion in conjunction with an elastoplastic finite element analysis. Figure 3 shows the configurations of the joints considered. It was found necessary to modify the mesh locally to remove points of singularity by introducing rounding of the same order of magnitude as the adhesive thickness. However no justification was given for this degree of rounding and it is not clear if the degree of rounding used, effects the energy density distribution and how reliable the critical plastic energy density (obtained from a bulk tensile test) is. This criterion was also used by [Zhao (1989)]. This is essentially a continuation of the work of [Adams et al (1987)]. It is pointed out that this criterion will not work with sharp corners and that an arbitrary rounding (of twice the adhesive thickness) should be used to analyse normal joints. It should be noted that this is a different degree of rounding than used by [Adams et al (1987)]. The degree of rounding will significantly effect the maximum energy density and thus extensive parametric testing is required.

2.2.9 Assessment

[Bigwood and co-workers (Bigwood 1990, Crocombe et al 1990)] have assessed a range of critical parameters for a number of joint configurations and adhesives. Details of the joint configurations are given in Figure 4. In this work cleavage joints and compressive shear specimens with two adherend thicknesses and three types of adhesives (epoxy, untoughened and toughened, and acrylic) were tested and analysed using both elastoplastic finite element analysis and an elastoplastic semi-closed form analysis. The problem of the singularity is not of concern for the semi-closed form analyses. The results of these analyses were then used to assess the suitability of various maximum stress and strain components as failure criteria. It was shown that the maximum principal stress gave a good indication of joint strength for the various joints made with untoughened epoxy adhesive. However for the toughened epoxy a maximum principal stress criterion seemed to apply in mode I whilst a maximum principal strain criterion applied in mode II. Thus, from this it would appear that no unique criterion applies even though the failure appears to be cohesive in each case.
There are also some fundamental problems with this type of failure criterion. Typical joints, such as single lap joints, contain bi-material geometric features that can result in singular stress/strain fields. In these circumstances a maximum stress/strain failure criterion is obviously meaningless. However it has been stated by [Zhao (1991) and Adams et al (1974)] that sharp corners do not exist in real joints and therefore neither do the stress/strain singularities. This is a valid point but as observed by [Adams and Harris (1987)], and discussed in section 2.3.3, the value of the maximum stress/strain components is highly sensitive to small changes in the degree of rounding whereas the experimental results are not [Adams et al (1974), Zhao and Adams (1989), Zhao (1991)]. It can be concluded therefore that although reasonable success has been obtained with maximum stress/strain failure criterion there is analytical and experimental evidence to suggest that this form of failure criteria is only suitable for adhesive joints when some averaged value is used. This leads directly into the next section.

2.3 Stress/strain and a distance

2.3.1 Introduction

A way of getting around the problems associated with maximum stress/strain criteria discussed above is to use a maximum stress/strain at a given distance from the point of singularity or a critical value of stress/strain averaged over a given region. This is in effect what has been done (albeit unknowingly) by most of the criteria, that were evaluated using FEA, discussed in the section above. The stress/strain has either been found at a certain fixed distance from the singularity (ie the nearest gauss point in a finite element analysis) or some averaged value has been used (an adhesive centre-line value). The maximum value given by a closed form solution will not be singular and so represents an averaged value. Such a criterion was first used in the composites field about twenty years ago by [Whitney et al (1974)] to deal with the effect of stress concentrations in composites. The approach has been developed since then and works well over a limited data range. It is not possible, for instance, to take the characteristic distance used for open holes and use it for bolted joints.

2.3.2 Previous work

[John et al (1991)] state that a shear stress reaches a critical value at a given normalised distance from the overlap end for their bonded double lap joints of unidirectional composite with varying overlap lengths. However, in practice, this means that the critical distance varies with the overlap length and thus is not a unique parameter. Further only elastic analyses were used and thus the adhesive stress distribution is not at all representative, indeed the authors state that the reasons for the common stress and distance value are not clear.

More deliberately [Zhao (1991)] used an average value over a distance to predict failure in single lap-joints with fillets and various degrees of rounding on the embedded corner. Two types of adhesive were used, a brittle epoxy, and a rubber toughened epoxy. Four different degrees of rounding were tested and analysed. However, an averaged stress failure criterion was only applied to the sharp corner and small radius configuration whilst a maximum stress criterion had to be used with the larger radii. For the joints bonded with the brittle epoxy the results from linear elastic finite element analysis were used. The principal stress around the embedded substrate corner in the fillet was averaged across the thickness of the adhesive layer and the ultimate tensile stress (obtained from bulk tests) was used to predict the failure load. The predictions obtained were too high. However good predictions were obtained using either a lower critical stress or a smaller averaging distance. It is very difficult however to find a
physical justification for the values used. For the CTBN toughened epoxy configurations the plastic energy density was used in conjunction with the results of elastoplastic finite element analysis to predict failure. Once again it was necessary to use averaged values for the larger radii and maximum values for the sharper corners. One feels that as the mode of failure is the same the same criteria should be applicable.

[Clarke and McGregor (1993)] have used a slightly modified approach in that they postulate that the maximum principal stress (from an elastoplastic analysis) should exceed the measured uniaxial failure stress of the adhesive over a certain zone size normal to the principal stress. Using two different toughened epoxy adhesives on single lap, double strap and flange joints they obtain predictions to about 5% of measured strengths with zones sizes of 1 and 0.68 mm with corresponding critical values of 67 and 70MPa. However it appears that the full stress-strain curves have not been obtained and thus the values used for critical stresses were clearly too low. Further, an arbitrary extrapolation to the curve was used for modelling purposes and this is likely to effect the finite element results significantly. Finally the zone size was determined from the zone size of a joint analysed to its failure load, there is no physical justification for these size zones. Two interesting points are made, firstly it was shown that using a stress at a critical distance causes considerable errors in predicted joint strengths, this can be seen in Figure 5 where various predictions for the variation of T peel strength with adhesive fillet size is given. Secondly it was demonstrated that small geometric changes don’t effect the size of the zone and thus won’t effect the predicted joint strength.

[Crocombe et al (1994a)] have considered numerous components of stress and strain from both elastic and elastoplastic finite element analyses of cracked and non-cracked untoughened epoxy joints subjected to various modes of loading, see Figure 6. Surprisingly good results were found by using a critical peel stress of about 20MPa at around 0.3mm from the singularity. However, this stress appears to have little physical significance and the distance is rather remote to characterise the singularity. A more meaningful criterion was when the effective stress reached the yield stress, however the critical distances were then different for the different modes of loading, this being attributed to the variation of plastic zone sizes with loading mode. Thus this does not provide a unique criterion for a given adhesive.

A failure criterion based on a stress at a given distance was discussed by [Kinloch et al (1980, 1981, 1983ab)]. They used this criterion to predict failure in cracked epoxy specimens over a range of test rates and temperatures. A theoretical variation of toughness with crack tip blunting (found assuming fracture occurs when the stress at a critical distance from the crack tip reaches a critical value) was shown to compare well with the experimental data. Critical values for various adhesives were given, these were usually between 200-500MPa at 10-0.1 microns from the crack tip. The authors were not sure if any physical significance could be assigned to these critical values. This criterion has not been shown to apply to non-cracked configurations.

2.3.3 Assessment

The greatest drawback of this approach is the lack of any physical significance to the critical parameters. Other drawbacks are that it has not been established that these parameters are unique material properties or whether they might vary with adhesive thickness, mode of loading etc. Also, the last of the approaches discussed above is only applicable to a configuration containing a macro-crack. Generally designers are interested in predicting the strength of notionally uncracked joints.
2.4 Limit state analysis

2.4.1 Previous work

Limit state analysis is a form of failure assessment presented first by [Crocombe (1989)]. At that time it was introduced as a global yielding failure criterion. The premise of this criterion is that for ductile adhesives it is quite possible that before local conditions are sufficient to cause failure the entire adhesive layer will yield and reach a limiting state which can sustain no additional load. The applicability of this criterion was demonstrated for three joint configurations, single lap, double lap and compressive shear. Each of these configurations were bonded with highly ductile adhesives. Non-linear finite element analyses were carried out and the load at which the adhesive layer was completely yielded was determined. Figure 7 shows the prediction for the single lap joint and it can be seen that although the thinner adhesive layer yields at an earlier load the thicker adhesive layer is the first to reach a state of complete yield. The results obtained compared favourably with the experimental failure loads. It was also noted by [Zhao (1991)] that this type of failure criterion was applicable to a CTBN toughened adhesive lap joints when a large radius was included at the ends of the adherends. This limit state approach fits well with the pseudo closed-form elastoplastic joint analyses. [Schmit (1992)] include global yielding as one of the possible failure mechanisms in their analysis of stepped double lap joints. This is also implemented in the JMENr program which is based on the analysis of Hart-Smith.

2.4.2 Assessment

This criterion is only applicable only to a limited range of adhesive joints. The majority of structural epoxy adhesives do not have sufficient ductility for the entire adhesive layer to yield prior to joint failure.

2.5 Fracture mechanics

2.5.1 Introduction

Fracture mechanics, as used to predict failure in homogeneous bodies has been applied extensively to adhesive joints. Fracture mechanics is the study of the strength of structures which contain flaws such as cracks. Instead of looking at the local value of peak stress, which are infinite at the crack tip, fracture mechanics assesses if the conditions in the structure are suitable for failure. The principles were set out by [Griffith (1920)]. He suggested that a brittle system containing flaws will fail when the energy the structure can supply to the crack tip under given loading (the energy release rate G) is equal to the energy required for the crack to propagate (the critical energy release rate Gc). A number of other criteria have also been proposed for the prediction of crack propagation including critical stress intensity factor (Kc), a critical value of J integral (Jc) and critical crack opening displacements (δc). Each of these is discussed in detail fracture mechanics texts such as [Broek (1982)]. It should be noted that under small scale yielding conditions in a homogeneous system Kc=(EG)1/2 for plane stress and K1=(EG/(1−v^2))1/2 for plane strain, J=G and δ−G/σ_{yield}.

2.5.2 Previous work

[Kinloch (1987)] states two advantages of using energy release rate over stress intensity factors for adhesive joints. Firstly, G has strong physical meaning being directly related to the energy absorbing processes. Secondly a usable value of K is not always straightforward to obtain. This
is particularly true if the crack is at or close to the interface. In a two-dimensional homogeneous problem \( K \) is usually divided into \( K_I \) associated with opening stresses and \( K_{II} \) associated with in-plane shearing stresses; for interfacial crack problems \( K_I \) and \( K_{II} \) do not have such a physical significance and both arise for nominally mode I loading (Kinloch 1987). Because of the complications with stress intensity factors Toya (1990) also stated that energy release rates were more appropriate parameters for predicting failure in bi-material crack problems.

The composites community have done a considerable amount of work in this area. Recent papers which cover general mixed mode fracture in composites include [Martin (1991), Williams (1989) and Hashemi et al (1991)].

As mentioned previously \( G_C \) is directly related to the energy absorbing processes and can be written as \( G_C = G_0 + g \), where \( G_0 \) is the intrinsic fracture energy and \( g \) is the energy dissipated in viscoelastic and plastic deformation at the crack tip. [Gent et al and Andrews et al (ref. from Kinloch 1987)] proposed that \( g = G_0 f(\alpha', T, \epsilon) \), where \( \alpha' \) is the crack growth rate, \( T \) is the temperature and \( \epsilon \) is the level of strain. This implies that \( G_C = G_0 F(\alpha', T, \epsilon) \) where \( F \) is a loss function. The inclusion of temperature and crack extension in the loss function indicates that the fracture energy is both rate and temperature dependent. The nature of these dependencies is discussed in detail in [Kinloch and Shaw (1981) and Kinloch (1987)].

The constraining nature of the substrates in adhesive joints cause a number of problems, including the need to consider fracture under combined loading conditions and a dependency of the critical energy release rate on the thickness of the adhesive layer, both effects were reviewed by [Kinloch et al (1981, 1987)] and the effect of joint width and bond thickness on the fracture energy are shown in Figure 8. Cracks in homogeneous isotropic materials tend to propagate in mode I, along a path normal to the direction of maximum principal stress, regardless of the orientation of the original flaw. However in an adhesive joint the direction of crack propagation may be constrained by the substrates. It is therefore important to consider fracture under combined loading conditions. Various mixed mode fracture criterion have been proposed, for example \( (G_{ICJ}/G_{IC})^A + (G_{ICJ}/G_{IC})^B = 1 \) (where \( G_{ICJ} \) and \( G_{IC} \) are the respective values in the joint of interest at fracture). Values of \( A = 0.5 \), \( B = 1 \) and \( A = B = 1 \) have been reported as appropriate [ref. Kinloch et al (1981, 1987)].

The constraining effect of the substrates is observed in the dependency of \( K_{IC} \) and \( G_{IC} \) on the adhesive layer thickness, see Figure 8b. The effect of the rigid substrates close to the crack tip is to increase the level of tensile stress and thus extend the length of plastic zone. As the substrates become further away from the crack tip this effect is reduced and \( G_C \) decreases. If the substrates become too close to the crack, then the height of the plastic zone is restricted and thus \( G_C \) decreases. It has been noted that the maximum values for \( K_{IC} \) and \( G_{IC} \) are obtained when the thickness of the adhesive layer is approximately equal to the plastic zone size. In these circumstances \( K_{IC} \) and \( G_{IC} \) can be larger than the bulk values.

The above discussions indicate that the application of fracture mechanics to adhesive joints causes some problems. Nevertheless fracture mechanics has been applied with reasonable success to adhesive joints, [Malyshev and Salganik (1965)]. [Mostovoy (1971)] investigated the variation of toughness with adhesive composition and was able to obtain a qualitative relationship between tensile properties and bulk and joint toughness. They note the increase in toughness with increasing adhesive layer thickness and the different types of load-displacement response studied by later workers. [Trantina (1972a)] carried out finite element analysis of aluminium-epoxy SEN specimens. The results of this were used to determine fracture energies. These were seen to
compare well with values found using the same adhesive in other opening mode joint specimens. [Trantina (1972b)] also carried analysis and tests on angled SEN specimens using the same aluminium and epoxy. His results would suggest that failure at a constant $G = G_I + G_{II}$ is not appropriate. However his analysis neglects the adhesive layer and this might affect the validity of the results. Also, as the mode mixity increased the failure became interfacial and this will also affect the measured toughness.

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[Mulville et al (1978)] considered various combinations of in-plane loading on interfacially cracked CTBN-aluminium SEN specimens. The failure loci depended on the mode of loading, becoming more interfacial as the shear loading increased. Although failure envelopes were drawn no attempt was made to determine mixed mode failure criteria. [Hamoush and Ahmad (1989)] investigated interfacially cracked compact tension, shear and flexural specimens made by bonding steel or concrete with an epoxy. They found that the total fracture energy was essentially constant and independent of adhesive thickness and mode of loading, see Figure 9. This is probably due to the large adhesive layers considered (>2.5mm) and the fact that the adhesive is relatively brittle.

The work by [Anderson et al (1988)] applied a fracture mechanics approach to uncracked systems by determining an inherent flaw size. A series of butt joint tensile ('button') tests were carried out on a brittle epoxy using specimens with various flaw sizes and an unflawed configuration. The results from these tests were then used in conjunction with finite element analysis to determine $G_c$ and an inherent flaw size $a_0$ (the flaw size that would produce the same failure load as the nominally unflawed configuration). These values of $G_c$ and $a_0$ were then used to predict failure in blister tests and thick lap shear tests. The correlation obtained between experimental and predicted failure loads was only good for joints experiencing mainly mode I and having interfacial failure. A similar procedure was then tried with a rubber toughened high strength structural epoxy in a peel joint using $J_c$ as the failure parameter. The results obtained were disappointing because of the dependency of $J$ on the constraining effect of the substrates. It was concluded therefore that additional investigation needed to be carried out into the nature of $J$ before this type of technique could be applied to toughened epoxies.

[Crocombe (1981)] considered the peeling of a thin spring steel strip bonded with a toughened epoxy adhesive to a rigid substrate. Non-linear elastic finite element analyses were used to show that failure occurred at a critical intensity for a range of peel angles. The critical value was obtained by least squares curve fitting the measured peel loads. Thus the value may well be dependent on a number of factors including mesh size. It was found that, locally, the mode ratio was largely unaffected by the peel angle (30-90°) and thus a single critical intensity was valid over this range.

[Fernlund et al (1991)] tested various geometries of CLS specimens in 4 point bend with and without applied tension. The specimens were pre-cracked before testing to failure. They derived values of $G_c$ from closed form elastic analyses. When no substrate yielding had occurred it was found that fracture was governed by $G_c$ that was dependent on adhesive thickness and mode of loading. Although attractive because of the simplicity of the analysis the fact that $G_c$ can in no way be considered an inherent material property is a severe disadvantage. This work is followed up in a later paper, [Papini (1994)], where the strength of thick aluminium lap joints of varying geometries bonded with a tougher epoxy adhesive have been measured and predicted. The variation of "steady-state" $J_c$ with mode mixity was used ($J_{IC}/J_{IC}$ is found to be about 6) and the strengths were predicted to within 10%. There was little difference between the failure loads if a fillet or a small pre-crack was present. However this is only likely to be the case for a tough
adhesive, more brittle adhesives will be affected more by the presence of a fillet. It was not clear how much mode variation occurred and thus how severe a test these experiments were. Additionally the value of $J_C$ is certainly a function of adhesive thickness. Work by [Schmit (1992)], based on elastoplastic closed form analyses, considers three competing criteria, limit state, substrate rupture and adhesive fracture. This last mode of failure appears to be governed by a mode II fracture energy. This is probably because the joints considered are symmetric about their centre line and are thus dominated by shear. This, of course, is not the case for many adhesive joint configurations. [Hu et al (1992)] have carried out closed form shear lag analyses for linear, elastic perfectly plastic and linear hardening systems. They obtain expressions for failure load in terms of $J_C$ and can differentiate between local and global (limit state) conditions. They appear to obtain good predictions of lap joint strengths for various adhesives. This analysis, unlike that of [Fernlund (1991) and Papini (1994)] accounts for parameters such as adhesive thickness and properties but is only valid for joints in shear. Thus it appears to be useful as a design tool but not really applicable in this program of work.

[Wang et al (1978,1981) and Small et al (1986)] have also carried out similar work, their configurations generally containing macro-cracks.

### 2.5.3 Assessment

In most of the papers discussed above a macro-crack has been present in all the joints. This is not particularly relevant to untracked systems. There are a number of objections to using inherent "Griffiths" flaws. One is that the size of crack calculated can often be much larger than the flaw sizes that actually exist. Further fracture mechanics principles can only really be applied to macro-sized cracks and for polymers this is in the order of mm. Another difficulty of applying the inherent flaw size technique, pointed out by [Groth (1988a)], is that the precise location at which to insert a crack may not be obvious.

Another difficulty is that $G_C$ is not usually a material constant. It usually varies with mode of loading, adhesive thickness, joint width and rate of application of load. A lot of this variation is due to the different size of plastic zones that develop. If some way could be devised to account for this plastic deformation separately it might be possible to approach a parameter that is more nearly material constant. This is one of the aspects considered in the work involving damage mechanics criterion which is covered in a later section.

### 2.6 Bi-material stress intensity

#### 2.6.1 Introduction

The use of a failure criterion based on a bi-material stress intensity is an extension of critical stress intensity factors (fracture toughness), used in fracture mechanics, to problems which do not contain cracks. The stress field around bi-material singularities and homogeneous cracks have similar forms which can be express as:

$$\sigma = \frac{Qr^{-\lambda}}{A}$$

where $Q$ is the stress intensity factor ($Q=K$ for homogeneous cracks), $r$ is the distance from the point of singularity, $\lambda$ is the strength of the singularity ($\lambda=0.5$ for homogeneous cracks and...
\( \lambda < 0.5 \) for bi-material singularities) and \( A \) is a scaling constant \( (A = (2\pi)^{1/2}) \) for homogeneous cracks. Thus if the order of the singularity is known the stress intensity factor \( Q \) characterises the stress field. The premise of this form of failure criterion is that failure occurs at a point of bi-material singularity when the (elastic) stresses reach a given level characterised by \( Q = Q_C \).

2.6.2 Previous work

[Gradin and Groth (1984)] appear to be the first to apply this type of criterion to a non-cracked configuration. A series of mixed mode cleavage tests, bonded with a toughened epoxy, under various modes of loading were carried out on a test configuration containing a well defined bi-material singularity, see Figure 10a. Finite element analysis was then carried out using the experimental failure loads to determine \( Q \) at the onset of failure. This is shown in Figure 10b for four modes of loading and it can be seen how the maximum \( Q \) is nearly the same in each case. The values obtained showed reasonable consistency, predicting failure loads to \( \pm 10\% \). It was suggested that a critical value of \( Q \) can be used to predict failure under a wide range of loading conditions. It is not clear whether the same value could be used for a different joint bonded with the same adhesive. Indeed the locus of failure in the joint has not been specified. [Groth (1985a, b)] subsequently applied this form of criterion to a range of single lap joint configurations (increasing overlap length and substrate thickness) without fillets, and showed that \( Q_C \) was reasonably independent of joint geometry. However, it was noted that \( Q_C \) became increasingly dependent upon adhesive layer thickness as the thickness became small and that a different value of \( Q_C \) had been used from the earlier paper although the adhesive was the same. This form of failure criterion was also applied to single lap-joints with fillets (Groth 1988a) with limited success. Good correlation was obtained for long overlap lengths but the predictions failed to pick up the rapid decrease in joint strength as the overlap length became small, resulting in errors as high as 25% in predicted strength. At the measured failure loads the aluminium substrates would have yielded considerably but these were treated as elastic in the analysis. Additionally, considerable adhesive plasticity would have occurred. The applicability of a critical bi-material stress intensity factor failure criterion in elastoplastic material was investigated by [Groth and Brottare (1988b)]. Although no experimental validation was performed it was shown that a stress intensity factor could be defined under conditions of small scale yielding.

Critical bi-material stress intensity failure criteria for non-cracked systems have also been investigated by [Hattori et al (1988a, b), Crocombe et al (1990) and Carpenter (1988)]. The work of Hattori et al followed a similar line to Groth in that \( Q_C \) was used to predict failure. The main point of interest to come from this work was the indication that \( Q_C \) was dependent on the strength of the singularity. A range of geometries were used to obtain different strengths of bi-material singularity whilst keeping the adhesive and substrate materials the same. These were then loaded thermally and the temperature at which failure occurred was recorded. Using the coefficients of thermal expansion in conjunction with finite element analysis the stress intensity factors at failure were determined. Predicted delaminations were up to 30% too low. One point that seems to have been overlooked by the authors is the temperature dependent nature of plasticity and rupture in epoxies. Since different geometries will require different temperatures to cause the same stress intensity factors the results could be severely distorted. [Crocombe et al (1990)] used a similar approach to predict failure in cleavage joints with different substrate thicknesses. The actual configurations were discussed in more detail in section 2.2 above. Strength predictions were obtained to within about 7% but only for a very limited range of mode I loaded joints.

A different approach to the application of stress intensity failure criterion was presented by
[Carpenter and Patton (1988)]. They presented a contour integral method of determining the stress intensity around a point of bi-material singularity. They went on to compare predictions based on stress intensity and finite element analysis with those based on a [Goland and Reissner (1944)] closed form analysis and a stress concentration approach and showed that the maximum stresses from a closed form solution vary in a different manner to the calculated bi-material stress intensities as the configuration of the joint is changed. Thus an averaged stress criterion based on a closed form analysis is not interchangeable with a stress intensity criterion.

2.6.3 Assessment

The work carried out on this form of failure criterion has indicated that there are a number of areas that need to be addressed before this form of failure criterion can be taken as generally applicable. These include the dependency of $Q_C$ on the strength of the singularity and investigating whether $Q_C$ is a material property. As $Q_C$ is similar to $K_C$ one might expect to encounter the same sorts of problems that occur with the application of LEFM, i.e., variation with mode mixity and dependency on adhesive thickness, joint width, rate, temperature and locus of failure within the bondline. However, it does provide a singularity based criterion that is applicable to non-cracked configurations. It is also unclear whether this type of criterion would be suitable for joint geometries with rounded adherends that do not have a well-defined singularity, or ductile adhesives that exhibit significant non-linear behaviour.

2.7 Damage modelling

2.7.1 Introduction

In most, if not all, of the above work, where necessary the adhesive material behaviour has been arbitrarily extrapolated beyond the measured stress-strain data. This is rather unrealistic and perhaps a more representative form of the data would be a reduction to a zero load carrying capacity. By introducing such behaviour - effectively incorporating the failure criterion into the analysis - the response of the joint is completely determined from the analysis (i.e., stable and unstable fracture can both be modelled).

Damage can be modelled at various levels of sophistication. This begins with simply removing the material when some critical condition is exceeded. A progression of this is to allow the material to soften gradually to a state when it can carry no load. A more rigorous approach is to define damage criterion and to combine this with yield criterion in a general constitutive model. This last approach is known as continuum damage mechanics and one application of this work has been in modelling ductile fracture and a vast amount of literature is available. All the above has concerned the macro response of the material. A final approach is to model explicitly the actual micro-mechanisms. Much of the work cited in this review is at the low levels of sophistication which can be implemented fairly readily.

This concept has also been used in the failure analysis of composite materials and key workers include [Backlund et al (1986), Soutis et al (1991) and Cui et al (1993)]. In this work it has generally been used to model the damage generated around stress concentrations.

2.7.2 Previous work

[Crocombe et al (1994a)] attempt to model the damage zone ahead of the singularity in their interfacially cracked and non-cracked joints using a line of softening springs which are only
activated when the surrounding principal stress reaches a critical value. These springs soften to rupture at a critical displacement (akin to CTOD in fracture mechanics). Figure 11 shows these springs in a finite element mesh in the vicinity of a point of singularity in an adhesive joint. It can be shown that the energy absorbed by these springs is related to the energy release rate and thus the model gives a traditional fracture mechanics response at large cracks but, unlike fracture mechanics, predicts finite failure loads at vanishingly small cracks. However there are two main shortcomings at the present stage of development; the critical displacement for spring rupture is different for mode I and mixed mode and the technique is worst for non-cracked configurations. At this stage the surrounding material is elastic and thus all the energy absorbing mechanisms are lumped into the springs. This will be improved when the surrounding material can deform plastically and could significantly improve the shortcomings discussed above.

[Laschet et al (1992)] model adhesive damage in an alternative manner by setting the material properties of those elements which violate a specified condition (maximum principal stress in this case) to zero. This is a severe way of modelling damage but they claim that, using the experimental data of [Harris (1984)], they obtain consistent values of joint strengths. They use two meshes, the first corresponding to that used by [Harris (1984)] and a more refined mesh. The coarse mesh over predicts both the initial failure (onset of damage) and the final failure load significantly. The refined mesh produces corresponding values of 5 and 5.8kN respectively which compare well with a failure load of 5.9kN. However this could be fortuitous as a mesh refined further still is certainly likely to under predict the initial joint strength (onset of damage) and probably the final joint strength as well. This last aspect needs further investigation. The onset and growth of damage for the refined mesh is shown in Figure 12.

[Edlund (1992)] provides the first true application of continuum damage mechanics to adhesive joints. Both yielding and damage can occur independently, the former is dependent on both deviatoric and hydrostatic levels of stress whilst the latter only on hydrostatic stresses. Yield and damage criteria are formally presented and implemented using the FEA code 'ABAQUS'. Two cases have been considered, both concerned with two theoretical bonded square plates, in one case rotated and in another cleaved apart at one corner. Only analyses have been carried out and there is no supporting experimental validation.

[Chow (1992)] have also developed continuum damage mechanics principles for adhesive joints but have not made any strength predictions at this stage. They apply and extend the principles they have already developed for metals. Damage criterion and damaged perturbed yield criterion are developed assuming that the damage is caused solely by dilatational stresses. These are implemented into a finite element formulation and the problem of a centre-cracked butt joint is considered. Plots of the damage stress and the damage are shown and it is seen that the maximum damage does not occur at the positions of the maximum damage stress and a new criterion for fracture is postulated.

**2.7.3 Assessment**

In summary, it would appear that modelling the onset and propagation of damage to predict joint failure is potentially a useful approach. Both in terms of model reality and also as a means of accommodating additional "post failure!!" load carrying capacity. However it is a concept still in its infancy and will need to be explicitly incorporated into existing finite element code. There are considerably more parameters available in continuum damage mechanics and these require greater effort to determine and validate.
2.8 Conclusions

1. It is inappropriate to use maximum stress/strain as a criterion in conjunction with a detailed finite element analysis. If sharp corners are modelled then the stresses and/or the strains become singular. If these features are rounded the maximum values are a function of the rounding used.

2. A maximum stress/strain criterion may be used in conjunction with a closed form type analysis which gives an average value.

3. The concept of stress/strain and distance appears to provide reasonable predictions but lacks obvious physical relevance.

4. Application of standard fracture mechanics to a non-cracked problem is particularly difficult. If using a finite element model the virtual crack closure method gives a zero energy release rate (i.e. infinite failure load) for a vanishingly small crack increment.

5. Use of an inherent flaw size is possible but the uniqueness of this for a given adhesive system needs to be determined.

6. It appears possible to apply fracture mechanics to non-cracked joints using a closed form approach but this requires further investigation.

7. When applying fracture mechanics to cracked problems the greatest difficulty is in the variation of fracture energy with various joint, loading and failure parameters. This is largely due to the variation in the energy absorbed in plastic deformation.

8. A critical bi-material intensity appears to vary with the singularity strength and thus cannot be a unique material property.

9. Modelling a local damage zone enables plastic energy to be separated from fracture energy and also appears to be equally applicable to non-cracked and cracked configurations.

3. Cyclic Fatigue Loading Failure Criteria

The work that exists in this area can be split into two main areas, criteria for crack propagation (using Paris law concepts) and for fatigue life (using S-N concepts). At first sight it may appear that work in the latter category is more useful. However the reader should be aware that fatigue life can be subdivided into crack initiation and propagation. The relative amount of life in each stage varies with joint type and load level from being completely initiation dominated [Harris et al (1992)], to completely propagation dominated [Crompton et al (1989)]. Thus to obtain a complete predictive capability it is necessary to quantify both stages. Generally the work using an S-N approach makes no attempt to separate initiation and propagation and thus may be a little restricted. The literature will be reviewed under these two different approaches.
3.1 Criteria for fatigue crack propagation

3.1.1 Introduction

The establishment of fatigue damage tolerant design procedures will require that fatigue crack propagation rates for different joint configurations, loading conditions, bond line thicknesses and crack lengths, be predicted. To obtain this, appropriate or genuine parameters are necessary which can accurately correlate fatigue crack propagation rate (FCPR) under a range of operating conditions. Fracture mechanics parameters such as stress intensity factor $K$, $J$-integral, strain-energy release rate $G$, etc. have successfully characterised crack driving force in homogeneous materials.

3.1.2 Previous work

[Mostovoy and Ripling (1975)] reported an extensive study using fracture mechanics approach to correlate FCPR in adhesively bonded joints. They used TDCB specimens which consisted of aluminium alloy adherends bonded with various structural adhesives, largely based on epoxy resins. The FCPR, $da/dN$ was plotted against the range of adhesive fracture energy, $\Delta G_I$, which was imposed. Over much of the range of the experimental data the FCPR could be expressed by a Paris law of the form:

$$\frac{da}{dN} = c(\Delta G_I)^m$$

where the parameters $c$ and $m$ are constants but their values are typically dependent upon material variables, temperature, frequency, stress ratio and environment. The data also revealed that the relationship between $da/dN$ and $\Delta G_I$ is actually sigmoidal in shape. Crack growth rates were found to decrease to very low values as $\Delta G_I$ approaches some limiting threshold value, $\Delta G_{TH}$, and to increase to very high values as $G_{MAX}$ approaches the typical $G_{IC}$ value for crack growth under short-term monatomic loading conditions.

[Crompton and Clark (1989)] examined the general applicability of $\Delta G$ to providing a correlation of crack propagation data using modified notched compact tension specimens consisted of two aluminium blocks bonded with a high strength one-part epoxy adhesive of thickness of 0.5 and 2 mm. The overall strain-energy release rate range $\Delta G$, obtained as if the specimen is of a homogeneous material, failed to correlate crack growth rate $da/dN$ of the specimen. Taking different Young's moduli of adherend (aluminium) and adhesive (epoxy) into consideration, they analysed the stress and strain fields ahead of the crack tip for their specimens and obtained finite element stress intensity calibrations. Good correlation exists between $da/dN$ and $AK$ for the joint with 0.5 mm bond line thickness. But the approach failed to justify FCPR of the specimen with 2 mm bond line thickness. There was some question as to the validity of the evaluation of $\Delta G$.

[Marceau (1977) and Osiyemi et al (1990)] are among the many workers who have also noted a similar behaviour for mode I loading, finding both threshold and Paris law regions. Both considered double cantilever beams, the former using aluminium substrates and the latter unidirectional cfrp. In both of these works $G$ has been calculated using beam theory. [Xu et al (1994)] have noted that even for such simple specimens as the DCB a simple correction of the monolithic form of the expression for $G$ is not accurate and recourse to techniques such as FEA
are necessary. Figure 13 shows the FCGR data for the bonded aluminium DCB specimens used by Xu et al. It can be seen that for this particular adhesive the data is not sensitive to test frequency, however other adhesives have been shown to be sensitive to frequency and this has been attributed to the effect of creep by many workers, this will be addressed in a later section.

[Luckyram et al (1988)] have carried out fatigue tests on bulk compact tension specimens of two adhesives at three frequencies. They consider a range of formula for FCPR taken from models used for metals that account for parameters such as the stress ratio and mean stress. They find that most of the models produce a good linear fit.

The above examples only considered the joints under mode I loading and generally considered just a single adhesive thickness. The fracture energy is known to be a function of adhesive thickness and thus we might not expect a single set of Paris law constants to be unique for a given adhesive. This is an aspect which needs further consideration.

Adhesively bonded joints are often subject to mixed-Mode loading where both mode I peel and mode II shear components exist under service conditions. Mixed-mode loading effects appear to have been studied first by [Brussat et al (1977, 1978)] who suggested an effective strain-energy release rate which was a function of the opening and shear mode energy release rates $G_I$ and $G_{II}$ respectively.

\[ \Delta G_{ef} = (1 + \frac{2G_{II}}{G_I + G_{II}}) \Delta G_I \]

Clearly this is a $G_I$ dominated function and was empirically derived on the basis of tests conducted on specimens under different ratios of $G_I$ to $G_{II}$.

[Mall et al (1982)] studied the relation between total strain-energy-release rate $G_T$ and debond propagation rate $da/dN$ of T300/5208 cfrp adherends bonded either with EC-3445 or FM-300 adhesive using CLS specimens and found good correlation with $G_T$ and this can be seen in Figure 14. Though $G_I$ and $G_{II}$ can also be used the error is smaller for $G_T$. In later work, [Johnson et al (1985)], the dependence on $G_T$ was further established by fatigue crack growth under a substrate tapered to 5° where the mode I loading is insignificant.

[Liechti and Lin (1986)] examined the fracture parameters concerning mixed-mode cyclic debonding in three types of joints bonded with same adhesive. In the cracked lap shear and structural lap joints, the fatigue cracks propagated cohesively. In the thick adherend single lap-shear specimen, the general pattern of fatigue crack propagation is initially adhesive followed by cohesive. The relative amounts of adhesive and cohesive propagation depend on the frequency of loading and temperature and humidity. Finite element analyses [Lin and Liechti, (1985)] indicated that energy release rates were quite insensitive to crack location. Total strain-energy release rate $G_T$ obtained by FEA has been found to correlate the debond rate $da/dN$ very well in the range of $G_I/G_{II} = 0.8$. However, repeatable discrepancy exists between FCPR’s of CLS specimens of different relative strap thickness. That has been attributed to the inaccurate modelling of end effects and possible effects of crack closure during fatigue.

[Wassell et al (1991)] claim to have used the J integral to correlate FCGR but it would appear that the analyses are elastic and hence J is the same as $G$. They obtain reasonably consistent data over the range of joint widths and bondline thicknesses considered. They state that the plastic zone is small compared with these parameters but this may not be the case with other adhesives.
Furthermore the smallest bondline considered was only 0.5mm and the data may not remain unique for smaller thicknesses.

[Schmueser (1991)] has carried out tests on CLS specimens using three different adhesive thicknesses. He has carried out extensive analyses of the joint and claims that the mode mixity is particularly sensitive to the number of elements across the adhesive layer and has to use 17 elements! This is significantly more than others have used. However, there are one or two unexplained trends in the sensitivity analysis presented and further clarification is necessary. He claims that FCPR correlates best with $G_t$ but there is significant discrepancy between the data from joints with different adhesive thicknesses. This illustrates the problem in finding parameters that are unique to a material.

3.1.3 Assessment

Most of the work above has simply shown that the Paris law can be used, usually with $G_t$ as the crack driving parameter. There is only a little evidence of using the Paris data from one configuration in a predictive sense on another and this has been far from satisfactory. Most of the joints contained initial cracks and very little work has been carried out in full life prediction.

3.2 Criteria for fatigue life

3.2.1 Previous work

[Matting et al (1968)] were early workers in the field who carried out fatigue tests on single lap joints. They tested at various levels of cyclic load and to over $10^6$ cycles. The data suggest that such joints exhibit an endurance limit (a stress below which fatigue failure will never occur). In this case it was around 10-15% of the static strength of the joint. No attempt was made to relate the performance to the actual maximum adhesive stresses however and thus transfer of this data might be difficult. Also noted was an increase in fatigue strength with test frequency, which with the samples tested also resulted in increased time to failure. The effect of varying the loading experienced is conventionally approached using Miner’s rule which assumes that damage proceeds linearly:

$$\sum n_i/N_i = 1$$

Here $n_i$ and $N_i$ are the number of cycles at a given load and the cycles to failure at that load respectively. It is shown that the summation is actually greater than 1 with decreasing loads and vice versa.

[Ibrahim and Miller (1980), based on the work of Zachariah (1974) and Miller and Zachariah (1977)], developed a procedure capable of separating the initiation and propagation phases. The procedure also enables the prediction of growth rate of very short cracks beyond the scope of linear-elastic fracture mechanics. Therefore it is of interest to the design of adhesively bonded joints which is based on fatigue crack initiation life span. The essential part of the procedure is to carry out a series of fatigue tests with increasing levels of loading and use a damage cumulation law that is different to the one proposed by Miner:
In the above equation, the subscript 1 denotes stage 1 where stress/strain level is low, x and y are the fractions of time spent during low and high stress/strain stages respectively, $a_0$, $a_i$ and $a_c$ are material intrinsic flaw size, crack size corresponding to the completion of initiation phase and critical crack size. The above equation represents a straight line in the x-y coordinate system with one end known to be $x=1$, $y=0$. To locate the other end, some two stage cycling tests must first be conducted using a high stress/strain level where $N_i$ may be neglected. By making use of the above equation a best fit procedure determines the end point of the straight line relationship of the propagation phase. As a simplification, assuming $a_i=a_0$, the above equation was reduced to give the number of cycles for crack initiation at low stress/strain stage:

$$[1-x(N)_1^{(x+y-1)}yN_1^{(x+y-1)}] \ln \left( \frac{a_f}{a_i} \right) = \ln \left( \frac{a_f}{a_0} \right) $$

Therefore it is possible to base the design on the established number of cycles for crack initiation phase.

[Harris and Fay (1992)] in their study of single-lap-joints bonded by Elastosol M51 and XW1012 adhesives fatigued at three temperatures, observed cracks starting from both edges of the joints. They also reported that the fatigue lives of the joints were controlled by initiation phase, seen as the development of small cracks at the corners of the joint. They also noted a significant decrease in the stiffness of the joint as the crack began to propagate. This might be a useful means of locating the end of the initiation phase to allow microscopy to be carried out on joints. Their work also indicated the existence of a fatigue life limit corresponding to certain percentage of static failure load of Elastosol M51 bonded joints, see Figure 15, although no similar results were found for XW1012 bonded joints. A further complication was that creep played a critical role in the fatigue test, except under the stress amplitude corresponding to the fatigue limit. No predictive work has been carried out in this study.

[Tiu and Sage (1986)] link bulk and joint adhesive fatigue data in their study of the fatigue crack initiation in 3 different types of double butt-strap joints made of unidirectional CFRP adherends and BSL 308A adhesive. Fatigue cracks initiated in the adhesive fillets at the location of the highest tensile stress. Fatigue crack initiation cycles were predicted based on S-N curve of the bulk adhesive by multiplying the fatigue strength of the bulk adhesive by a stress concentration factor in the fillet obtained by FEA. The predicted fatigue initiation strength of the joints did not correlate as closely as might be hoped with the experimental results, Figure 16. It can also be seen from this Figure that the proportion of life spent in crack propagation decreases as the fatigue stress increases. The site of fatigue crack initiation indicated that the initiation was tensile stress controlled. This argument was strengthened by the fact that when the fatigue load was reversed so that the original high tensile stress in the fillet was turned into a compressive one, the fatigue initiation strength was greatly improved. In addition, the fatigue crack still appeared in the region where the tensile stress is high after the load was reversed.

[Imanaka et al (1988, 1989)] takes a similar approach to fatigue life predictions of adhesive joints. They correlate the response of either a single lap joint in tension or a tubular lap joint in
tension or torsion with butt joints in tension and torsion. They use an epoxy and a polyamide. It is shown that the joints in tension compare best with the butt joint in tension when maximum adhesive tensile stresses are considered. However for the torsion loaded tubular joint the maximum shear stresses are used. Presumably the maximum tensile stresses (occurring at 45° to the shears) could have been used. However there was still a significant unresolved adhesive thickness effect apparent in the results even after using maximum rather than average adhesive stresses which would prevent this technique being used for fatigue life prediction.

There are some examples of obtaining fatigue lives from the sigmoidal FCPR data of which the Paris law is an important part. [Gilchrist (1993)], has attempted to model fatigue crack propagation from initial flaw to final catastrophic failure. Initial data defining the stress field around the debond was found from FEA. Propagation was found by using standard equations for growth of enclosed elliptical voids, semi elliptic shapes and finally through width cracks. From the calculations it is clear that 95% of the fatigue life is spent in propagating the initial elliptical void so that it breaks out on the front surface of the joint. Figure 17 illustrates this very clearly. A number of assumptions are made and it is not clear how changes in these assumptions would effect the results. Nevertheless the full life approach is one to be recommended. It should be stressed that no experimental evidence is provided to support this work. Further it would be interesting to see if the initiation:propagation ratio changed with the level of the applied cyclic load.

[Kinloch et al (1993)] have taken a similar approach with DCB manufactured from bonded fibre composite substrates. Assuming a Griffiths flaw size, a model for the complete sigmoidal FCPR curve for the adhesive and an expression for G for the DCB they integrate to obtain an expression for fatigue life which shows good correlation to the measured value, Figure 18. They state that this approach is insensitive to the initial flaw size and thus their joint cannot be dominated by initiation phase. By comparing this with the previous work of Gilchrist we see clearly the need to know whether a given joint is initiation or propagation dominated.

### 3.2.2 Assessment

A significant amount of work has been carried out using an S-N approach to estimate fatigue life. (however most of the work does not distinguish between initiation and whole life and as this is likely to vary with load level, amongst other parameters, then it might be of limited use). Once again most of the work has been carried out for one adhesive thickness and where more than one has been used no unique fatigue parameters were found. Finally it should be stated that integration over the complete loading history seems to offer an alternative way to predict fatigue lives. Nevertheless the effect of the initial flaw size (or its initiation) needs considerable study.

### 3.3 Creep-fatigue interaction

#### 3.3.1 Previous work

Work in this area is a hybrid between section 3 (cyclic loading) and section 4 (sustained loading). Many adhesives exhibit a frequency dependent fatigue response. This is noted by many workers including [Xu et al (1994)] who carry out fatigue crack propagation tests at frequencies of 20 and 2Hz (one adhesive was found to be much more rate sensitive than the other), [Marceau et al (1977) testing at 30 and 0.0028Hz, and Smith et al (1977)]. A number of workers monitor the cyclic strain, [Harris et al (1992), Imanaka (1984) and Althof (1984)]. It would appear that at high stress levels there is a build up of cyclic strain to some critical value that might itself be
frequency dependent. Figure 19 taken from the work of [Harris (1992)] shows this sensitivity of creep strain on applied level of fatigue load for single lap joints.

As to the extent creep affects fatigue behaviour of adhesively bonded joints, there seems to be no detailed study so far. Therefore methodology adopted in fatigue study of conventional materials, such as metals, may be useful. Concerning creep-fatigue crack growth under tension-compression loading, [Ohtani (1980), followed by Okazakiet et al. (1983) and Ohji et al. (1984)], classified the process of crack growth into two categories, cyclic-dependent type and time-dependent type, and claimed that the former is correlated with the fatigue J-integral range, \( \Delta J_f \), and the latter the creep J-integral range \( \Delta J_c \). Similarly, [Gladwin et al. (1989)] divided fatigue-creep crack driving force into two components. Total rates of crack growth can be calculated by summing the cyclic and dwell period contributions.

3.3.2 Assessment

Some properties of some adhesives exhibit a significant rate dependency. The rate experienced by an adhesive is governed largely by the frequency but is also effected by the stress distribution within the joint, ie thinner bond lines experience higher stresses and thus higher rates even at the same frequency. At high frequencies there is often not enough time to allow creep to occur and thus its effect is inhibited. However as the rate decreases creep becomes increasingly dominant. It is difficult to separate creep from fatigue.

3.4 Conclusions

1. Fatigue crack growth rate can be correlated with a crack driving parameter using a form of Paris law. However it is unlikely that any of the derived coefficients are genuine material constants.

2. Values of G determined by assuming beam theory can be significantly in error.

3. To apply such an approach to non-cracked joints it is necessary to use an inherent flaw size and it is not clear whether this is a constant for a given adhesive system.

4. S-N curves have been used to give fatigue life data but this doesn’t distinguish between initiation and propagation and thus is unlikely to be fixed for a given adhesive

5. Creep fatigue interaction can be particularly important in low frequency fatigue. Very little predictive work for adhesive systems has been carried out.

4. Criteria for sustained loading

Predicting failure requires both a constitutive model for the material (that describe how an adhesive responds to an applied stress or strain) and a criterion for failure. In sections 2 and 3 the constitutive models used have not been discussed because they are relatively straightforward, usually based on established time independent elasticity or elastoplasticity. This section of sustained loading is concerned with loads that may vary (or remain constant) over a relatively long periods of time. The constitutive behaviour for such forms of loading is still being developed. Thus the work in this area will be reviewed in two sections. The first covers the work that has been carried out to develop constitutive models and the second the criteria that have been used to determine the point of failure.
4.1 General constitutive models

Although the range of loading that can be applied to a joint is limitless there are two very common forms of sustained loading; creep (a constant applied load) and relaxation (a constant applied strain). These loads are relatively simple in themselves but when applied to a joint (that has a non-uniform distribution of stress) the situation is more complex. The general approach is to define the behaviour of the bulk adhesive and then to use this in conjunction with a stress analysis to determine the time dependent distribution of stress and strain within a joint.

There are various approaches that are used to model the behaviour of polymers and adhesives in particular. These will be dealt with in increasing order of complexity.

4.1.1 Isochronous behaviour

A simple equation of state where the strain is a function of the stress and time is reasonable when the loading is constant or continuously increasing. However it cannot model unloading or abrupt changes of loading/unloading at all well. [Botha (1983) and Crocombe (1994b)] amongst others have used this approach, with some success, in the form of isochronous stress-strain curves. The relationship between creep, relaxation and isochronous behaviour is shown in Figure 20. Isochronous curves give the stress-strain relation corresponding to a given time. Thus such a curve can be used in a conventional time independent elastoplastic stress analysis. One chooses a representative time and then uses the curve at this time. This is a rather ad-hoc approach and is the main problem with this technique.

[Botha (1983)] modelled the strain distribution in a thick adherend shear specimen obtaining his isochronous curves from bulk tensile creep tests. [Crocombe (1994b)] modelled the time dependent behaviour of single lap joints made with FM73 loaded at different rates. The bulk adhesive data was obtained from [Althof (1982)] who determined the creep and relaxation behaviour in shear using the thick adherend shear test.

4.1.2 Explicit strain rate dependent models

This sub-section is concerned with loading at a constant strain rate. It is generally found that increasing the strain rate increases the general level of stress that can be sustained but reduces the rupture strain. [Sancaktar (1985)] cites Ludwig’s equation for strain-rate dependent rupture (and yield stress) for polymers as:

\[ \tau_r = A + \ln(B\gamma) \]

[Crocombe (1994a)] find that this expression is valid for the epoxy adhesive they are working with and derive an empirical model (a function only of the strain rate) for the stress-strain behaviour. Figure 21 shows both the experimental data and the model. This has been used in an iterative manner in conjunction with non-linear finite element stress analyses to determine an average strain rate for the plastic zone present in their joint tests. A similar approach has been used by [Xu et al (1994)] to accommodate the rate dependent behaviour of one of the adhesives that were tested at various frequencies under cyclic loading. In both of the above the material behaviour was characterised by a single optimum stress-strain behaviour. The strain rate at any particular point in a joint will experience a different strain rate and thus an improvement on the above would be to zone the material and to use different stress-strain curves for each zone. Even this degree of complexity will only be valid if the strain rate at a given point does not change.
significantly. Thus it is likely to be applicable for joints loaded at constant displacement rates but not for other forms of loading. This is likely to prove a little restrictive.

4.1.3 Linear viscoelastic models

There are many linear viscoelastic models based on simple spring and dash-pot analogies. A good account of such models is given by [Williams (1980)]. A single spring and dash pot either in series (Maxwell) or in parallel (Voigt) cannot model both creep and relaxation. This is clearly illustrated in Figure 22. However more complex functions of springs and dashpots or alternative analytical expressions can give a better fit. One common form it to put Voigt models in series (Voigt-Kelvin) or Maxwell models in parallel (Maxwell-Weichert). When solved for creep or relaxation respectively the yield the following results.

Creep compliance:

\[ C(t) = \frac{\varepsilon(t)}{\sigma_o} = \sum C_i (1 - e^{-t/\tau}) \]

Relaxation modulus:

\[ E(t) = \frac{\sigma(t)}{\varepsilon_o} = \sum E_i e^{-t/\tau} \]

These give multiple increases in creep compliance or decreases in relaxation modulus which are common characteristics of polymers. A number of finite element codes such as ANSYS and ABAQUS will allow viscoelastic material properties of this form. The only problem is to determine the constants that need to be input.

Another approach is to use a power law representation for the creep compliance.

Creep compliance:

\[ C(t) = \frac{\varepsilon(t)}{\sigma_o} = At^n \]

This can be expressed in terms of a Voigt-Kelvin model using a limited number of terms. This has been done by [Rochefort et al (1983)] for adhesive FM73, as part of a non-linear viscoelastic representation which will be considered later.

However it is generally not possible to use constants determined from one form of loading to predict the behaviour in another form of loading. Furthermore at even modest levels of stress the behaviour becomes non-linear and it is not possible to use the same constants even for the same form of loading.
[Althof (1981)] has carried out creep, recovery and relaxation tests on thick adherend shear joints made with FM73. He used a 4 parameter model, Figure 23, and a power law representation of the creep and recovery strains:

\[ \gamma(t) = \gamma_0 e^{At^m} \]

He found that a power law form could be used for creep and recovery, but with different constants and that the 4 parameter model was a poor fit. Further, down to 15MPa no linear behaviour was noted. Thus it was recommended that isochronous models be used.

[Brinson (1982)] uses a modified Bingham model to define the rate dependent behaviour of a toughened epoxy. This model contains slip elements and consists of three regions; linear elastic, viscoelastic and a perfectly plastic region. The dashpot and final slider constants were not constant but were found for each strain rate. Thus this does not provide a unique set of material parameters. It was found however, that the same data could not be used to predict the response of single lap joints bonded with the same adhesive.

So far we have considered specific forms of loading, usually creep or relaxation from which creep compliance \( C(t) \) and relaxation modulus \( E(t) \) can be found. Superposition principles allow the strain resulting from a known stress history (or the stress resulting from a known strain history) to be obtained from a time integration involving the creep compliance (or the relaxation modulus) which are often represented as a power law. Thus:

\[ \varepsilon(t) = \varepsilon_0 \int_{t_0}^{t} C(t-\tau) \frac{d\sigma}{d\tau} \, d\tau \]

Sometimes the compliance is a function of a reduced time parameter which enables time-temperature superposition to be affected. This is more usually found in its non-linear form and this is discussed further next section.

It should be noted that the main difficulties associated with any linear viscoelastic model is that it is applicable only to low levels of applied stress or strain. Further it would appear that model constants for one loading case cannot be used for another form of loading, even at these low levels of stress. A power law representation seems to give a better fit than a simple spring-dashpot model.

4.1.4 Non-linear viscoelastic models

To obtain non-linear visco-elastic behaviour it is necessary to use a non-linear integral. The most commonly used integral is that attributed to [Schapery (1965)]. The non-linear integral is given below and can be seen to be an extension of linear superposition given above.

\[ \varepsilon(t) = f_0 C_0 \sigma_0 + f_1 \int C(\psi - \psi') \frac{d(f_2 \sigma)}{d\tau} \, d\tau \]

In this expression \( f_0, \psi' \) and \( \psi \) are functions of stress and \( C_0 \) and \( C \) are the instantaneous and transient components of the linear creep compliance. [Peretz et al (1983)] carry out bulk tests on FM73 and develop a procedure to evaluate the stress dependent terms in the integral. [Reddy et al (1987)] have accommodated this model into a finite element code NOVA and use this to analyse two stage creep, ramped stress and temperature and also creep and recovery. They
obtain excellent correlation with experimental data. Figure 24 shows the importance of including non-linear behaviour. It should be noted however that this has only been used where the stress history is known ie not for relaxation or ramp strain inputs. There exists another non-linear viscoelastic code VISTA, attributable to [Becker (1984)] but this is apparently a subset of the approach above where all the non-linearity is in the reduced time, \( \psi \).

Another approach adopted for non-linear viscoelasticity is a non-linear form of the power law:

\[ \gamma(t) = \gamma_0 + f(t) \]

where \( \gamma_0 \) and \( f \) are functions of stress. Rochefort(1983) shows that this can be made equivalent to Schapery's integral evaluated for creep loading and finds expressions for the stress dependent functions.

### 4.1.5 Viscoelasticity

This can be implemented on a range of levels. Most simple are the creep models that are available on most finite element codes, ie ANSYS, ABAQUS etc. These generally take the form:

\[ \frac{d\varepsilon}{dt} = f(\sigma, \varepsilon, t, T) \]

Such material models can be implemented relatively easily but are only really applicable to constant loading situations where the adhesive in the joint is subjected to a state of creep. This is the approach adopted by [Su et al (1993)] who wrote their own FEA code and analysed thick adherend shear joints subject to creep. Material data was taken from bending specimens and the creep compliance was assumed independent of stress contrary to the experimental data. A trilinear fit rather than a power law fit was used. Stress and strain distributions were shown but no attempt appears to have been made to validate the results.

[Groth (1990)] develops his own code, based on FEA principles, assuming shear stresses only in the adhesive. Three different models are implemented, viscoelastic, viscoplastic and viscoelastic plastic. These are illustrated in Figure 25. Constants are specified for the adhesive FM300 but no correlation to the material data is given. Once again stress and strain distributions are shown which illustrate the redistribution of stress but it is not clear how reasonable these results are. This emphasises the need to be able to validate the results of any material model.

The most promising (and rigorous) approach appears to be an adaptation of a metals technique known as the viscoplasticity based on over-stress (vbo) model. This has been shown by [Kitagawa et al (1989)] to be applicable to polypropylene for all tests where there is no load reduction. It has been validated against experimental data for constant strain rates, step constant strain rates, constant strain rate and hold, creep and relaxation. The excellent fit to the experimental data is shown in Figure 26. It is based on a non-linear three element model giving the following governing equation:

\[ k\tau' + \tau = m\gamma' + f(\gamma) \]

The term \( f(\gamma) \) is an equilibrium (ie very slow rate) stress-strain curve, \( k \) and \( m \) are related through the shear modulus \( G \) and are functions of strain and over-stress. This latter term is the
difference between the actual stress and the equilibrium stress. As yet this does not appear to have been used to model adhesive joints.

Finite element codes such as ANSYS and ABAQUS have similar non-linear viscoplastic models but also allow user written laws for viscoplastic strain rate to be input.

4.1.6 Assessment

Many of the models discussed above are only applicable to constant or constantly increasing loads. This is acceptable if creep loading only is of interest. Explicit strain rate models require some form of zoning to implement and even then will only be applicable under constant strain rate type loading conditions. The best approach is to develop a viscoelastic or viscoplastic model. The linear viscoelastic models are generally valid over a very low range of applied stress. Material constants for one type of loading often are not applicable to another. Non-linear viscoelasticity is fairly complicated to implement whilst viscoplasticity, although not simple has already been extensively developed by the metals community and is now being used by the polymers community. Both models require a reasonable amount of material data in order to generate the required material parameters. However these will be genuine material parameters.

4.2 Criterion for failure

4.2.1 Introduction

Although a lot of experimental work has been carried out in this area to study the effect of creep on polymer systems, relatively little work exists in the area of criteria for predicting the failure of such systems, most work concentrating on the prediction of time/displacement or time/load during the primary and secondary phases. Again less work still has been performed on the creep performance of adhesively bonded joint systems and the associated criteria.

4.2.2 Previous work

[Allen et al (1976)] carried out creep tests on steel single lap joints and found that a delay period occurred before a period of straining that was linear when plotted against the logarithm of time (secondary creep) and final accelerated straining to rupture (tertiary creep). The delay period was load dependent but, except at high loads the time in secondary creep will dominate the delay period. This secondary creep rate appeared to be linearly proportional to the creep load. There was some evidence that, when subjected to varying creep loads, the strain to failure was largely unchanged. However this was a joint strain and does not directly relate to an adhesive characteristic. Nevertheless this would suggest that log time to failure (t_f) decreases linearly with applied stress. This is supported by the findings of Wake (1979) for lap joints and others, see [Kinloch (1983b)], for bulk polymers and is more conventionally expressed as [Zhurkov’s (1965)] creep rupture equation which is based on a first order rate equation:

\[ t_f = t_{0e} \frac{1}{\sigma^a} \]

[McAbee et al (1970)] have a slightly different version of this equation where the time to failure is divided by the applied stress but this would appear dimensionally incorrect.

[Lewis (1972)] has followed a different approach, carrying out tests at varying strain rates and
extrapolating to find an endurance limit, i.e. a stress below which creep will not occur. From their work this was estimated to be 40% of the short term joint strength. However since then [Wake(1979)] has pointed out that there may have been an error in this approach. In the same vein, [Sancaktar (1985)] shows how [Crochet's (1966)] delayed failure equation:

\[ Y(t) = A + Be^{-\alpha t} \]

can be used to determine an endurance limit and further claims that it is better at describing the temperature dependent creep rupture than Zhurkov's equation. A, B and C are constants, Y(t) is the time dependent rupture stress and \( \alpha \) is the equivalent transient strain. A similar approach is presented by [Brinson (1982)] who uses data for a modified Bingham model found from constant strain rate tests to determine an explicit expression for the equivalent transient strain in creep as a function of time. This is substituted into the above expression and the time to rupture at various creep stresses predicted. The fit to the data is not bad and is shown in Figure 27.

[Gledhill et al (1976)] have carried out creep tests on cracked TDCB's and found that there was an incubation period and then sudden failure. The log of the time to failure was found to be inversely proportional to the applied energy release rate, somewhat consistent with the findings of Allen and Wake reported above. They were able to show that failure can be predicted when the CTOD reaches a critical value, constant for the test being carried out.

Another approach used to predict failure under sustained loading is presented by [Foux et al (1972) and Bruller (1978)] who extended the concept of failure at a critical stored deviatoric energy put forward much earlier by [Reiner (1939)]. This was applied to a range of polymers, including epoxies, subject to creep and constant strain rate loading. This concept has been used by [Hiel (1983)] in conjunction with Schapery's non-linear constitutive equation to determine an alternative expression for time to failure. [Crocombe (1994b)] has used this in conjunction with an isochronous model and non-linear finite element analyses to predict the strength of double lap joints loaded at markedly different rates and exhibiting notably different failure loads.

4.2.3 Assessment

Failure prediction is at a very early stage with joints subject to sustained loading. Three basic models have been reviewed and none have been significantly validated to the extent of producing material constants governing failure. They have mainly been applied to creep loading conditions. More work will have to be done to validate these further.

4.3 Conclusions

1. Most of the work has been carried out on creep loading of adhesives.

2. Isochronous and explicit rate dependent material models are limited but can be useful in certain situations.

3. Yield and rupture appear to be proportional to the logarithm of the applied strain rate.

4. Linear viscoelastic models are of extremely limited use.

5. Both non-linear viscoelastic and viscoplastic models have been shown to be valid for polymers.
6. Many authors report a delay period before creep straining occurs.

7. The time to failure under creep decreases exponentially with applied creep stress.

8. There is some evidence that failure may occur when the stored strain energy reaches a critical value.

5. Impact loading

5.1 Introduction

Impact tests are employed to measure the ability of a specimen or a finished component to withstand a sudden blow. In many applications a satisfactory resistance to impact loading is an important performance requirement and, indeed, impact toughness is often the deciding factor in materials selection. Impact testing and the impact performance of polymeric and metallic materials have therefore been the subject of several reviews [see for example: [Turner (1973), Reed (1979), Kinloch and Young (1983), Brown (1988), Chona and Corwin (1993)]].

5.2 Experimental methods

5.2.1 Introduction

Four basic categories of experimental methods which have been used for testing bulk adhesives and adhesive joints may be identified:

1. Block-shear impact.
2. Flexed-beam impact.
3. High-speed tensile impact.
4. Falling-weight impact.

This classification covers most of the experimental methods which have been employed as basic impact tests for materials and components. However, it does exclude certain high-rate tests, e.g. split Hopkinson bar, shock-tube tests and the use of explosive charges. It is also of interest to note that, while impact tests are generally considered to cover the high-strain-rate end of the strain-rate spectrum, the data tabulated [Reed (1979)] in Table 2 indicates that the flexed-beam and falling-weight impact tests do not necessarily provide particularly high strain rates. The same comment may also be applied to the block-shear impact test. In the notched beam tests, however, the strain rate at the notch tip is considerably higher and has been estimated to be of the order of $5 \times 10^3$ s$^{-1}$.

Apart from impact tests for material assessment, there are to be found, in the literature and in International Standards, tests for examining manufactured components, e.g. pipes. However, such standards for impact testing of adhesively-bonded components have yet to be developed.

From several of these types of impact test an 'impact strength' is measured. Now, this term is essentially correct when undertaking a test where the failure stress is determined from conducting, for example, a lap-shear or tensile-butt joint test under a high rate of test. However,
an initial point to clarify is that the commonly used term 'impact strength' is a loose misnomer when the test requires the energy absorbed by the specimen before it breaks to be ascertained. Since, in these types of test, the impact energy is actually determined. The impact energy may then be expressed as the energy per unit area of the broken cross-section, the energy per unit length of any notch present in the specimen, the energy per unit width of the specimen or the energy per unit volume of the test specimen. Hence, such tests as the block-shear or flexed-beam tests actually lead to a value of the impact energy being deduced.

<table>
<thead>
<tr>
<th>Test method</th>
<th>Order of magnitude of strain rate ($s^{-1}$)</th>
<th>Impact velocity (m/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flexed beam:</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Charpy</td>
<td>10</td>
<td>3</td>
</tr>
<tr>
<td>Izod</td>
<td>60</td>
<td>2</td>
</tr>
<tr>
<td>Falling weight</td>
<td>$10^{-1}$ - 10</td>
<td>1 - 4</td>
</tr>
<tr>
<td>Conventional tensile</td>
<td>$10^{-3}$ - $10^{-1}$</td>
<td>$10^{-5}$ - $10^{-1}$</td>
</tr>
<tr>
<td>Pneumatic gun</td>
<td>$10^{2}$ - $10^{4}$</td>
<td>20 - 240</td>
</tr>
<tr>
<td>Hydraulically operated</td>
<td>$1$ - $10^{2}$</td>
<td>0.008 - 25</td>
</tr>
</tbody>
</table>

Table 2. Order of magnitude characteristics of various impact tests.

5.2.2 Block-shear Impact

This test is an ASTM Standard method (D 950, 1982) and British Standard Method (BS 5350: Part 4 : 1986). The specimen is shown in Figure 28 and the upper part of the bonded joint is struck by the striker-head of a pendulum hammer at the bottom of its arc, with a striker-head velocity of 3.4 m/s. The impact energy absorbed from the pendulum (which is taken to be that needed to fracture the specimen) is deduced from a knowledge of the initial and final heights of the pendulum head. The impact strength (but more properly termed the "impact energy") is then determined by dividing the impact energy by the bonded area of the joint. The problem with this test is that it is very dependent upon the exact geometry of the test specimen and it will often be associated with strong dynamic effects. The data may be of use for simple comparative purposes, but do not provide any basic engineering properties.

5.2.3 Flexed-beam Impact

Two flexed-beam impact tests, the Charpy and Izod methods, are the most commonly used methods for determining the impact energy of polymers and metals. In both a pendulum-type impact machine is used and the specimen is usually notched. The specimen geometry and support arrangements are shown in Figure 29. As for the block-shear tests described above, the specimen is struck by the striker-head of the pendulum hammer at the bottom of its arc, which then continues to swing to a measured maximum height. The pendulum is initially released from a fixed height and the energy absorbed from the pendulum is deduced from the difference between the release height and the maximum height at the end of it first half oscillation. Again, this energy is taken to be that needed to fracture the specimen.

The basic difference between the Charpy and Izod tests is the manner in which the specimen is supported and clamped - held horizontal but not clamped in the former and clamped in a
vertical position in the latter (see Figure 29). In the Izod test, variations in the clamping force may cause significant differences in the measured energy absorption but the use of a standard clamping force may help in minimising this source of variability.

The flexed-beam tests for polymers which contain either no notch or a blunt notch are covered in BS 2782 (1970) and ASTM D 256 (1973). The Charpy and Izod tests usually give different values of the impact energy for unnotched or blunt-notched specimens; and it is these types of specimen which are required to be used in these Standards. However, if 'naturally sharp' cracked specimens are used then the value of the fracture energy, GIc, or fracture toughness, KIc, may be determined. In these instances the Charpy and Izod specimens yield the same value of GIc or KIc. Indeed, the research on the use of a fracture mechanics approach has led to the widespread use of such methods for the impact testing of plastics [Williams (1984)], including bulk adhesives [Kinloch et. al. (1987), Jakusik et. al. (1990)], and these methods are now specified in an European Structural Integrity Society (ESIS) Draft Standard (1993). This test method, using 'naturally-sharp' cracked specimens, has also been used by [Kodokian and Kinloch (1987) and Jamarani et. al. (1990)] for adhesive joints.

Finally, the last few years has seen a steady increase in the use of 'instrumented' impact machines. These typically have a strain-gauge or transducer mounted in the head of the pendulum so that the impact force versus time (of the order of milliseconds) may be measured. However, as with all impact tests, one must be aware of the problems introduced by 'dynamic effects'. Such effects may often arise during impact, or other high-rate type, tests and may lead to erroneous results, as discussed in detail below.

5.2.4 High-speed impact using an applied tensile load

In these tests the adhesive joint is typically subjected to tensile loading, typically using a constant rate of displacement of up to about 25 m/s. In the literature many different joint geometries have been tested in this manner, including lap-shear joints [Harris and Adams (1985), Beevers and Ellis (1984)], tensile-butt joints [Wegman and Tanner (1963), Makatao et. al. (1984)], peel joints [Marwick and Powell, (1988)], and bonded DCB joints [Blackman (1993)].

Most recently, an ISO Standard (1993) [ISO Standard] has been issued which describes the determination of the peel resistance of a wedge-peel test under a high rate of test (i.e. 2 to 3 m/s). The basic test arrangement for the wedge peel impact test is shown in Figure 30. Obviously, this is basically a peel test where the wedge is driven through the joint. The standard for this test method has only recently been issued, but it is to be expected that the measured impact peel load, or impact peel energy, will be very sensitive to the details of the specimen geometry. For example, the thickness, material type and alloy type of the substrates will greatly affect the ability of the metallic substrates to undergo plastic deformation and, therefore, will greatly affect the measured failure loads, or energy.

Again, a main experimental difficulty is the occurrence of dynamic effects once the time to failure becomes less than a few milliseconds; the time to failure being defined as the time taken for the load to climb from zero to its maximum value. These dynamic effects cannot be simply removed by filtering the load versus time signal. All that filtering achieves is the attainment of a smoother load versus time trace, with no indication of whether the trace is accurate or not. The dynamic problems associated with recording the failure loads may be overcome to some extent by the use of high-speed photography to record the displacement (and crack growth) in the specimen [Blackman (1993)].
5.2.5 Falling-weight impact

The falling-weight method is typically used by the polymer and composite industries where the specimen is too thin or too flexible to allow flexed-beam impact tests to be undertaken. The specifications for such tests are given in BS 2782 (1970), ASTM D 1709 (1975) and ASTM D 3029 (1972). Essentially, the specimen is a circular, square or rectangular plate which is typically clamped around the periphery and is struck by a hemispherically nosed metal dart of variable weight falling vertically. The speed of impact depends only upon the distance through which the mass falls but the associated energy and momentum depend also upon the mass. Thus, knowing the weight of the dart and the height through which it falls it is easy to calculate the energy available for fracturing the specimen. Again, more recently, instrumented falling-weight equipment has been commercially introduced by placing a strain-gauge or transducer in the head of the falling dart, hence the load versus time trace may be recorded.

The main use of the falling-weight method in the area of adhesives has been the impacting of bonded box-beams and bonded tubes, which were stood on one end and then impacted on the other end [Harris and Adams (1985), Fay and Suthurst (1990)]. However, bonded-box beams have also been subjected to a three-point bend impact test [Moody et. al. (1987)] and the testing of bonded plates has also been reported [Jordan (1988)]. Also, the falling-weight test equipment, if instrumented, may be used to undertake the wedge peel impact test, see above.

5.3 Maximum stress/strain criteria

5.3.1 Previous work

Obviously, the maximum stress /strain criteria which were discussed earlier may all be applied to the failure of joints subjected to high rates of test.

This approach has been adopted by [Harris and Adams (1985)]. They used an instrumented tensile-impact test and measured the strength and energy absorption of single-lap joints loaded in tension. They used four different epoxy adhesives and three different aluminium-alloy substrates. An interesting point was that they stated that the frontal barrier test for cars uses a speed of 30 mile/h (13.4 m/s). However, since the structure will have a certain stiffness, then the loading rate will be proportional not only to the impact velocity, but also to that stiffness. Clearly the stiffness of a single-lap joint will be much greater than the very much larger car structure, so that a reduced impact velocity is required for the lap joint tests. They estimated the lap joint specimen stiffness to be at least ten times the structural stiffness that was being represented, and hence selected an impact velocity for the lap joint tests of 1.34 m/s. They found that, even though the difference in test rate between the static and impact loading was of the order of $10^5$, that only small and relatively insignificant differences in joint strength were observed, see Table 3. Further, from these tests the best combination for attaining high strength joints was a toughened epoxy adhesive together with a high yield-stress aluminium-alloy substrate. However, when they examined the energy absorbed they found that high energy absorption was associated with a toughened epoxy adhesive together with a low yield-stress aluminium-alloy substrate. This arises because much of the energy absorption came from plastic deformation of the aluminium-alloy substrate - and this leads to the obvious selection of a low yield-stress alloy. Harris and Adams then used finite element analysis (FEA) to predict the strength of the lap joints under both static and impact loading. Their results are shown in Table 3.
<table>
<thead>
<tr>
<th>Adhesive (Epoxies)</th>
<th>Failure criterion</th>
<th>Static strength (kN)</th>
<th>Impact strength (kN)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Expt.</td>
<td>Theory</td>
</tr>
<tr>
<td>Simple</td>
<td>stress</td>
<td>5.05</td>
<td>4.08</td>
</tr>
<tr>
<td>Plasticised</td>
<td>stress</td>
<td>5.6</td>
<td>5.9</td>
</tr>
<tr>
<td>Toughened</td>
<td>strain</td>
<td>8.85</td>
<td>9.9</td>
</tr>
<tr>
<td>Toughened</td>
<td>strain</td>
<td>14.7</td>
<td>15.9</td>
</tr>
</tbody>
</table>

Table 3. Predictions of joint strength for joints bonded with 2L73 aluminium-alloy substrates.

The FEA model assumed a spew fillet, i.e. assumed that some of the adhesive was exuded from the joint during its manufacture to give a fillet of adhesive at the ends of the bonded overlap. The model treated both the simple and plasticised epoxies as linear elastic materials and the failure criterion was based upon the attainment of a maximum principal stress in the adhesive layer. The two toughened adhesives were modelled as non-linear materials and the failure criterion was based upon the attainment of a maximum principal strain in the adhesive layer. The agreement between experiment and theory shown in Table 3 is reasonable, but the authors’ commented that a problem, particularly for the impact loading cases, was the measurement of accurate, reliable and appropriate bulk properties of the adhesive to use in the predictive analyses. Also, it should be noted that they chose the substrate which exhibited little or no plastic deformation - the high yield-stress '2L73' aluminium-alloy. Obviously, if the substrates which exhibited significant plastic deformation had been selected, then the modelling would have had to allow for this relatively extensive plasticity. Such modelling is not an easy task.

[Wegman and Tanner (1963)] have also found that when bonded epoxy/steel tensile-butt joints were tested at relatively high rates of test the failure stress was greater compared to results from relatively slow rates. Indeed, this general observation is what would be expected from the effect of test rate on the properties of polymeric materials. The viscoelastic nature of polymers generally leads to a higher tensile or shear strength being observed, with the modulus also increasing, as the rate of test is increased, or the test temperature is decreased. However, a common feature is that the failure strain, and toughness, of the polymer decreases under these test conditions, see for example [Kinloch and Young (1983)]. A further complication with adhesive joints, of course, is that the locus of failure may change as the test rate is changed.

5.3.2 Assessment

The main problems are:

1. As discussed earlier, the problems of (a) obtaining singular values of stress and strain and (b) the sensitivity of the values of stress and strain to small geometric effects when analysing the joints using FEA cause considerable difficulties when applying maximum stress/strain criteria.

2. Further, measuring the failure properties of the 'bulk' adhesive, which are needed to act as the failure criteria, is always a problem. For example, (a) measuring the shear failure strain of the adhesive is always fraught with difficulties and uncertainties, and (b) the tensile failure strain is usually deduced from tensile dumbbell-shaped specimens and is
notoriously sensitive to the details of specimen preparation and the degree of specimen polishing that is employed.

3. An additional problem for predicting the failure of high-speed tests, is obtaining the values of the basic mechanical properties of the adhesive at high rates. For example, undertaking 'thick adherend shear tests' in order to measure the shear failure strain at high rates is not a trivial exercise.

4. When interfacial failure is observed the cohesive properties of the adhesive would not appear to be appropriate failure criteria.

5. At relatively high rates of test problems associated with dynamic effects arise, see Section 5.6 below.

6. Plastic deformation of the metallic substrates also needs to be taken into account.

5.4 Stress/strain and a distance criteria

5.4.1 Introduction

Similarly, criteria based upon the attainment of a critical stress/strain/strain-energy density which must act over a distance may be applicable to the failure of joints subjected to high rates of test. However, such criteria have not yet apparently been applied to the results from high-rate tests.

[Marwick and Powell (1988)] have reported the results of T-peel tests conducted at high rates of test of up to 6 m/s and fractured by driving a wedge through the peel specimen. The tests were also undertaken over a wide range of test temperatures. However, whether the critical stress/distance criterion reported by their colleagues [Clarke and McGregor (1993)] for slow speed tests also describes these test results has yet to be determined. Similarly, whether such a criterion is also applicable to the wedge peel impact test has yet to be investigated.

5.4.2 Assessment

The essential advantages and problems in applying a failure criterion based upon a critical stress/strain/strain-energy density which must act over a distance are:

1. If the stress involved is the yield stress, or yield strain, or some function of these two parameters, then at least these parameters can be reliably measured at both slow and high rates of test and should be readily transferable from the 'bulk' to the 'thin adhesive layer' state. Unlike, for example, the failure stress or failure strain or strain-energy density at failure, all of which are very dependent upon the detailed geometry of the bulk specimen as well as the volume of specimen, as was discussed previously. Parameters such as the yield stress, or yield strain, may also be reliably modelled as a function of test rate, and test temperature, as well as the state of stress, e.g. the degree of triaxiality of the stress state.

2. From all the work in the literature on both polymers and metals which is known to the present authors, the distance over which the critical stress/strain/strain-energy density must act appears to be simply an empirical fitting parameter. Furthermore, the value of this parameter cannot be ascertained a priori. Thus, whilst this approach holds promise,
it has yet to be firmly established on joints tested at slow rates of test. This should obviously be undertaken before the added complication of high test rates is introduced.

3. Peel joints, particularly, tend to exhibit plastic deformation of the substrates, obviously the prediction of joint failure requires that such deformation is accounted for in the model.

4. Again, when interfacial failure is observed the cohesive properties of the adhesive would not appear to represent an appropriate failure criteria.

5. Again, any dynamic effects must be modelled.

5.5 Fracture mechanics criteria

5.5.1 Introduction

As commented earlier, the basic aim of fracture mechanics is to identify fracture criteria such as the fracture energy, $G_{IC}$, and fracture toughness (or critical stress-intensity factor), $K_{IC}$, which are independent of the geometry of the cracked body. Values of such 'material parameters' should, therefore, greatly assist in developing a more fundamental understanding of the fracture process and be of considerable benefit in the practical areas of (a) material formulation and selection, (b) quality assurance, and (c) engineering design and life-prediction studies. Thus, to establish the validity of the linear-elastic fracture-mechanics (LEFM) approach very different geometries of test specimen are typically examined, using a very wide range of crack lengths and other specimen dimensions, in order to ascertain the values of $G_{IC}$ and $K_{IC}$, and demonstrate that they are indeed 'material parameters'.

A [BS Standard (1987)] exists for the determination of the dynamic fracture of metallic materials. In this Standard the compact tension and single-edge notched three-point bend (SENB) are employed and the dynamic value of $K_{IC}$ and crack opening displacement are measured. There is also an [ESIS Draft Standard (1993)] for the determination of the dynamic value of $G_{IC}$ and $K_{IC}$ for plastics, again the compact tension and single-edge notched three-point bend (SENB) are employed.

As discussed above, the fracture energy approach is the more readily applicable to the failure of adhesive joints. However, if the stress-intensity factor approach is applied there are several points which must be considered. Firstly, whether a singular region is indeed dominant at the crack tip (or at the stress concentration point under study) must be established. Such a singular-dominant region is a necessary requirement for this approach to be valid, and in thin adhesive layers this region is often very limited in extent and may be overwhelmed by any plastic, or damage, zone which forms at the crack tip, see [Charalambides et. al. (1992)] for a further discussion of this point. Secondly, the value of the geometry factor (used to deduce the value of $K_{IC}$) for an adhesive joint is not necessarily the same in value as that for the bulk material. Thirdly, the simple relationship between $K_{IC}$ and $G_{IC}$ which exists for a bulk material is frequently more complex, and of an unknown form, for an adhesive joint [Kinloch (1987)]. Fourthly, if interfacial failure is found to occur, then the fracture energy approach is readily applicable, whilst the stress-intensity factor approach becomes more complex and lacks a firm physical basis.

The specimen geometries which have been examined for bulk adhesive materials and adhesive
joints subjected to impact loading are discussed below.

5.5.2 Test geometries

5.5.2.1 Single-edge notched three-point bend (SENB) specimen

The single-edge notched three-point bend (SENB) specimen may either be used for bulk or joint specimens, as is shown in Figure 31. This specimen geometry has been the most popular for the impact loading of both bulk adhesives and for adhesive joints. In the case of adhesive joints, both cohesive cracks and interfacial cracks have been employed.

The fracture energy, $G_{IC}$, is given from a LEFM analysis by:

$$G_{IC} = \frac{U_c}{D W \Phi}$$

where $U_c$ is the stored elastic strain-energy in the specimen at the onset of crack growth, $D$ is the thickness, $W$ is the width of the specimen and $\Phi$ is a dimensionless geometry factor, $\dot{\Phi}$, such that:

$$\Phi = \frac{C}{dC/d(a/W)}$$

In this equation, $C$ is the compliance of the specimen, such that $C = d/P$ where $d$ is the displacement and $P$ is the load. The value of $\Phi$ may be evaluated either from measuring the compliance as a function of crack length or, more readily, from published tables of the value of $\Phi$ as a function of $a/W$ and $L/W$, where $L$ is the length or span of the test specimen between the support points [Williams (1984), ESIS (1993)]. The point of onset of crack growth is either taken as the maximum load, $P_{max}$, or the load, $P_{5\%}$, at the intersection where the straight line of $C + 5\%C$ crosses the load versus displacement trace. For the bulk adhesive, the test may be conducted according to the 'Draft ESIS Standard for High-Rate $K_C$ and $G_C$ Testing of Plastics' [ESIS (1993)]. This standard could also be the basis for the impact testing of SENB adhesive joint specimens. It should be noted that this standard imposes limitations on the minimum size of the specimen and on the degree of non-linearity of the load versus displacement trace, so as to ensure that the assumptions of LEFM are met.

To obtain values of $G_{IC}$, values of $U_c$ are plotted versus $BW\Phi$. The value of $U_c$ is determined by integration of the load versus displacement trace up to the point of the onset of crack growth. (The values of $D$ and $W$ may be changed for successive experimental tests. Changes in crack length, the usual parameter which is progressively changed, are reflected in the value of $\Phi$; the value of $\Phi$ being a function of crack length). There should be a linear fit to the data so plotted, and the straight line should pass through the origin. The slope of the linear fit yields the value of $G_{IC}$. Typical results, for a time to failure of about 1 ms, are shown in Figure 32.

5.5.2.2 Adhesively-bonded double cantilever beam (DCB) specimens

The adhesively-bonded double cantilever beam (DCB) specimen has also been used for testing adhesive joints under high rates of test. The dynamic effects were found to be very severe at
rates of test greater than about 2 m/s. Indeed, at these rates and higher, the load versus time traces were dominated by peaks due to inertial and stress wave effects. Therefore, the beam analysis equation used to ascertain values of \( G_{IC} \) was re-formulated to give:

\[
G_{IC} = \frac{3 \Gamma}{16 \Omega^2} \frac{\delta_c^2 h^3 E}{(c + \Delta h)^4}
\]

where \( 2h \) is the total thickness of substrate arms, \( a \) is the crack length, \( \delta_c \) is the critical displacement and \( E \) is the axial modulus of the substrate arms. The correction factor, \( A_i \), is introduced for end-rotation and deflection of the crack tip. Effects of large deflection of the specimen arms and of any bonded end-blocks (used to apply load to the specimen) may be taken into account by using the correction factors \( \Gamma \) and \( \Omega \); see [Blackman et. al. (1991) and Hashemi et. al. (1990)]. The important feature of this equation is that it does not require a knowledge of the load in order to deduce a value of \( G_{IC} \). The values of \( \delta_c \) and crack length, \( c \), were measured using high-speed photography and the value of \( E \) was determined from independent tests, including tests undertaken at ultrasonic frequencies.

It has been found [Blackman (1993)] that the value of \( G_{IC} \) was relatively unaffected for a high glass-transition temperature \( (T_g = 120^\circ C) \) toughened adhesive as the rate was steadily increased up to about 10 m/s, but decreased significantly above about 0.5 m/s for a toughened room-temperature cured adhesive \( (T_g = 50^\circ C) \). Finally, it should be noted that if the beam analysis equation utilising the load values was used, incorrect values of \( G_{IC} \) were deduced. Again, this simply arises from the load versus time trace being dominated by erroneous dynamic effects at high test rates.

5.5.2.3 Mixed-mode test specimens

The use of specimens which involve a mixed-mode (i.e. mode I/II) failure under impact loading have also been reported by [Jamarani et. al. (1990)]. In these specimens an initial (sharp) interfacial crack was placed in the adhesive joint. The results are shown in Table 4 below.

<table>
<thead>
<tr>
<th>Material</th>
<th>Type of SENB specimen</th>
<th>Type of initial crack</th>
<th>( G_{IC} ) (kJm(^2))</th>
<th>( G_{IC} ) (kJm(^2))</th>
</tr>
</thead>
<tbody>
<tr>
<td>Brittle epoxy</td>
<td>bulk</td>
<td>cohesive</td>
<td>0.48</td>
<td>-</td>
</tr>
<tr>
<td>Tough epoxy</td>
<td>bulk</td>
<td>cohesive</td>
<td>1.38</td>
<td>-</td>
</tr>
<tr>
<td>Tough epoxy</td>
<td>joint</td>
<td>cohesive</td>
<td>1.43</td>
<td>-</td>
</tr>
<tr>
<td>Tough epoxy</td>
<td>joint</td>
<td>interfacial</td>
<td>0.65</td>
<td>1.35</td>
</tr>
</tbody>
</table>

(Note: typical times to failure were of the order of 1 ms.)

Table 4. Values of \( G_{IC} \) and \( G_{IC} \) from impact tests on bulk and joint specimens.

As may be seen, the toughened epoxy clearly possesses a higher impact \( G_{IC} \) value than the unmodified, brittle epoxy; and the results from the bulk and joint specimens, when both contain a cohesive initial crack, are in good agreement. However, when an interfacial starter crack is
placed in the joints, then the value of $G_{IC}$ is considerably reduced, compared to the value obtained from using a cohesive starter crack. Finally, the mode II, $G_{IIIC}$, value is significantly higher than the mode I, $G_{IIC}$, value.

5.5.3 Assessment

The advantages and problem areas are:

1. In employing the fracture mechanics data for subsequent calculations on uncracked joints, the use of an inherent (or Griffith) flaw size is needed. The accurate calculation of such a parameter is uncertain and the parameter does not have a strong physical meaning - i.e. typically the inherent flaw actually develops during the test; it does not exist at this critical length in the unloaded, uncracked joint. Thus, for example, employing values of $G_{IC}$ for optimising the initial joint design is possibly of limited value.

2. We know that the value of the adhesive fracture energy, $G_{IC}$, is often dependent upon the joint geometry, for example the thickness of the adhesive layer [see, for example, Kinloch and Shaw (1981)]. Thus, we need to be able to model and predict the dependence of the value of $G_{IC}$ upon such geometry details.

3. If plasticity occurs in the substrates, then we need to be able to allow for this in predicting the failure load. Hence, for example, we need an interactive model to predict the performance of wedge peel tests which are impact loaded from a knowledge of the impact $G_{IC}$ value of the adhesive and the plastic behaviour of the metallic substrates.

4. The insertion of 'naturally-sharp' cracks into bulk adhesive specimen is relatively straightforward, but the techniques for working with adhesive joints need to be developed.

5. The measurement of mode II values needs to be considered.

6. The problem of elastic-plastic fracture mechanics, as opposed to LEFM, has yet to be addressed.

7. The strengths of a fracture mechanics approach would seem to be: (a) in providing a 'material parameter' for material development and selection, (b) in providing a 'material parameter' for quality assurance work, (c) predicting crack paths (i.e. whether the crack will tend towards one interface, or even meander between the interfaces), (d) assessing the extent of crack growth, and hence service life of a joint, and (e) readily coping with interfacial failure in joints.

8. As always in impact loading, the dynamic effects need to be taken into consideration.

5.6 Dynamic effects

High-rate fracture testing presents special problems because of the presence of dynamic effects. Such effects include: (a) vibrations in the test system which produce oscillations in the recorded parameters, (b) inertial loads which give rise to forces on the test specimens different from the forces sensed by force-transducer or strain-gauges which are used to measure the forces, and (c) interactions between the crack and reflected stress waves, which may for example cause
'stick/slip' crack growth. These effects need either to be controlled and, if possible, reduced by appropriate action, or else to be taken into account through proper analysis of the measured data.

A clear indication of dynamic effects occurring in the test is when oscillations (sometimes referred to as 'noise') are recorded in the load versus time trace. Some authors have removed such unwanted oscillations by filtering the load signal. However, this is extremely dangerous, since the portion of the load signal directly relevant to the test may also be removed! Indeed, the draft ['ESIS Standard on High Rate Testing of Plastics' (1993)] prohibits such filtering or attenuation unless the source of the removed 'noise' is known and the effect on the data is understood.

The relative importance of such effects increases with increasing testing rate decreasing test duration, and a decreasing specimen time to failure. At speeds of less than about 1 m/s (loading times of greater than about 1 ms) the dynamic effects may be negligible and the testing procedures used for static testing are directly applicable. At rates of around 1 m/s (loading times of the order of 1 ms) the dynamic effects become significant, but still controllable, and static test methods may used with some modifications. At test rates of several metres per second and higher (loading times appreciably shorter than about 1 ms) the dynamic effects become dominant, and special approaches to the determination of fracture energy values are required, such as directly measuring the displacement of the specimen using high-speed photography - see above.

For example, a common form of impact testing is for a pendulum striker to be allowed to impact upon a supported bar and many types of instrumented impact devices have the force-measuring transducer mounted on the head of the pendulum-striker. It is therefore most important to recognise that the forces actually measured are those acting on the striker, and the energy deduced from such measurements is that lost by the striker. However, for the calculation of the true 'material property' values of the impact strength or fracture energy from any of the above, or similar, equations the force that is needed is that acting in the specimen, and the energy needed is that gained by the specimen. Now, the problem that arises is that the measured values are not necessarily the same as the values required for an accurate analysis, and the derivation of the 'true' impact energy. At relatively slow impact velocities differences between the measured values and those actually acting in the specimen may be negligible but differences may become very pronounced as the impact velocity of the striker is increased, as detailed above.

The role of dynamic effects during pendulum-impact tests have been convincingly demonstrated by the work of [Venzi et. al. (1970), Kalthoff (1977, 1985), Kinloch et. al. (1987) and Williams et. al. (1987, 1987a, 1988, 1990)]. The main dynamic effects in these pendulum-type impact tests are: (a) the relatively high contact stiffness of the striker/specimen interface compared to that of the specimen and (b) the loss of contact and the regaining of contact between the specimen and the striker, and the specimen and the shoulders of the mounting vice, during the impact test. Such losses of contact have been observed directly by [Kalthoff (1985)] and are shown schematically in Figure 33. The loss of contact occurs due to the specimen accelerating and decelerating relative to the striker and also, associated with these changes in the displacement of the specimen, changes occur in the kinetic energy of the specimen. The important consequence of these dynamic effects is that, since the force transducer is mounted on the striker, the bending forces and associated stored strain-energy actually acting in the specimen may be incorrectly deduced. Also, severe oscillations and even multiple zero values may be observed in the measured force versus time relationship. Also, it should be noted that similar effects may occur during high-rate testing using tensile testing machines, since the constant rate of test is usually applied via driven wedges or lost motion devices.
Hence, the measured force versus time relationship is complex and two basic problems arise. Firstly, it may be difficult to ascertain the particular point on this relationship which corresponds to the onset of crack initiation. One way of overcoming this problem is to use a crack initiation gauge or high-speed photography, but both of these methods make the test more complex. Secondly, the measured force at the onset of crack growth and the measured energy, ascertained by integrating the measured force versus displacement relation, are not necessarily equivalent to the force or stored strain-energy acting in the specimen. As mentioned above, this problem may be avoided, by using only relatively low impact velocities, which result in relatively long time-scales for the impact event and so enable such effects to be neglected at the instant of crack initiation. However, when higher impact velocities are examined, and the associated time-scale of the impact test is relatively short, dynamic effects may then be significant.

In order to quantitatively model the above mentioned dynamic effects [Williams and Adams (1987)] have proposed using a mass-spring model. This model yields the following expression for the true impact fracture energy, \( G_{IC}^d \), corrected for dynamic effects:

\[
G_{IC}^d = \frac{U_c}{BWF} \frac{\alpha}{\alpha + 1} \frac{f(\xi)}{1 - 2\alpha/(\cos^2 \xi - 1)}
\]

The first term on the right-hand side is equivalent to the measured value of the fracture energy, \( G_{IC} \). This is determined by ascertaining the energy lost by the striker from the experimentally measured force versus time relation and by then equating the energy lost by the striker to the energy stored in the specimen at the onset of crack growth; i.e. taking the energy deduced from the measured force versus time relation up to the point of crack initiation as being equal to the value of \( U_c \) in Section 5.5.2.2, as is the usual procedure in interpreting impact test results. The second term arises from variations in the deformation of the specimen and the third term is a consequence of changes in the kinetic energy in the specimen. The various parameters are defined by:

\[
\alpha = \frac{k_1}{k_2}
\]

\[
\xi = \omega t_f
\]

\[
f(\xi) = (1 - \frac{\sin^2 \frac{\xi}{2}}{\xi}) + 4(1 - \frac{\cos^2 \xi}{\xi})(0.5\sin^2 \frac{\xi}{2})
\]

where \( \omega \) is the natural frequency of the system, \( k_1 \) is the contact stiffness between the striker and specimen, \( k_2 \) is the stiffness of the SENB specimen and \( t_f \) is the time to failure.

The effects of the dynamics of the test can clearly be seen from the data shown in Figure 34, which are taken from the work of [Kinloch et. al. (1987)]. The points represent values of \( G_{IC} \) calculated using the equation in Section 5.5.2.2. As may be seen it appears that as one increases the speed of the pendulum striker, and so decreases the value of \( t_f \), that the epoxy adhesive actually increases in toughness. Intuitively, this would not be expected from a materials science
standpoint. Now the above equation may be used to predict theoretically the form of the $G_{IC}$ versus $t_f$ relationship, and all the parameters may be directly measured or calculated. The theoretical predictions are shown as the solid lines in Figure 34. The fit of the theoretical predictions are in excellent agreement with the experimental values, and the main point to note is that the increase in the values of $G_{IC}$ at short times is due to dynamic effects and is not a real material effect. It simply arises because the force on the striker is actually measured during the test, rather than the force acting in the specimen. Thus, unless the dynamic effects are taken into account, and accurately modelled, the value of $G_{IC}$ measured from such tests may be greatly in error when relatively high rates of test are employed.

5.7 Conclusions

5.7.1 Test methods

There are many high-rate test methods to be found in the literature, and some are discussed in International Standards or Draft International Standards. In all cases it is vitally important of consider the role of dynamic effects when conducting such tests and when deducing the results from such tests. For bulk adhesives, then the Standards for metals and plastics may be consulted and generally followed. For adhesive joints, the two Standards which exist are the well-established ASTM 'Block-shear impact test' and the new ISO 'Wedge-peel impact test'. Neither of these test methods actually measure any basic engineering parameter, which could be utilised in (a) material selection, (b) quality assurance and (c) engineering design and life-prediction studies.

5.7.2 Failure criteria

Maximum stress/strain criteria:

1. The problems of (a) obtaining singular values of stress and strain and (b) the sensitivity of the values of stress and strain to small geometric effects when analysing the joints using an FEA method cause considerable difficulties.

2. Measuring the failure properties of the 'bulk' adhesive, which are needed to act as the failure criteria, is a problem.

3. Obtaining the values of the basic mechanical properties of the adhesive at high rates is difficult and prone to error.

4. Failure criteria based on the cohesive performance of the adhesive would not appear to be appropriate to predicting the performance of joints that fail at the interface.

5. Any criteria based upon the attainment of a critical stress or strain acting in the bulk adhesive or joint would not appear to be a generally useful or workable criteria.

Stress/strain/strain-energy and a distance criteria:

1. If the stress involved is the yield stress, or yield strain, or some function of these two parameters, then these parameters can be reliably measured at both slow and high rates of test and should be readily transferable from the 'bulk' to the 'thin adhesive layer' state.
Parameters such as the yield stress, or yield strain, may be reliably modelled as a function of test rate, and test temperature, as well as the state of stress, e.g. the degree of triaxiality of the stress state.

The distance over which the critical stress/strain/strain-energy density must act appears to be simply an empirical fitting parameter.

The value of distance over which the critical stress/strain/strain-energy density must act cannot be ascertained a priori.

The criterion has yet to be firmly established on joints tested at slow rates of test. This should be undertaken before the added complication of high test rates is introduced.

To be robust it would be expected that a model would need to account for all factors occurring in the joint, eg plastic deformation of adherends, dynamic effects - or explanation is needed to explain why certain effects can be ignored.

When interfacial failure is observed the cohesive properties of the adhesive would not appear to represent an appropriate failure criteria.

Fracture mechanics criteria:

1. The accurate calculation of an inherent flaw size is uncertain and the parameter does not have a strong physical meaning.

2. We need to be able to model and predict the dependence of the value of $G_{IC}$ upon geometry details.

3. If plasticity occurs in the substrates, then we need to be able to allow for this in predicting the failure load.

4. The insertion of 'naturally-sharp' cracks into bulk adhesive specimen is relatively straightforward, but the techniques for working with adhesive joints need to be developed.

5. The measurement of mode II values needs to be considered.

6. The problem of elastic-plastic fracture mechanics, as opposed to LEFM, has yet to be addressed.

7. The strength of a fracture mechanics approach would seem to be in:
   
   (a) providing a 'material parameter' for material development and selection;
   (b) providing a 'material parameter' for quality assurance work;
   (c) predicting crack paths (i.e. whether the crack will tend towards one interface, or even meander between the interfaces);
   (d) assessing the extent of crack growth, and hence service life of a joint;
   (e) readily coping with interfacial failure in joints.

8. Dynamic effects again need to be considered.
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Figure 1

Figure 2
Figure 3

Basic dimensions, mm

Modified finite element models

a

$y = 4x^2$
for $0.25 \leq x \leq 0$

$y = \sqrt{2}x^2$
for $|x| \leq 0.25\sqrt{2}$

b

c

Predicated locus of failure

Figure 4
Figure 5

Figure 6
Figure 7

Figure 8a

Figure 8b
Figure 9a

Figure 9b

Figure 10a

Figure 10b
Figure 11
Figure 12

- 2 Hz
  \[ \log_{10}(da/dN) = -14.1 + 5.22 \log_{10}(\Delta G) \]

- 20 Hz
  \[ \log_{10}(da/dN) = -14.4 + 5.28 \log_{10}(\Delta G) \]

Figure 13
Figure 14

Figure 15
Figure 16

Figure 17
Figure 18

Figure 19
Figure 20

Figure 21
Figure 22

(e) Creep behaviour

(b) Relaxation behaviour

Figure 23
Figure 24

Figure 25
Figure 26

Figure 27
Figure 28

Figure 29
Figure 30

Figure 31
Figure 32

Figure 33
Figure 34