

THOUGHTS ON THE DURABILITY AND DAMAGE TOLERANCE ASSESSMENT OF ADHESIVELY-BONDED JOINTS

Rhys Jones^{1,2}, Anthony J. Kinloch³, John G. Michopoulos⁴, Daren Peng¹

¹Centre of Expertise for Structural Mechanics, Department of Mechanical and Aerospace Engineering, Monash University, Clayton, Victoria, 3800, Australia

²ARC Industrial Transformation Training Centre on Surface Engineering for Advanced Materials, Faculty of Science, Engineering and Technology, Swinburne University of Technology, John Street, Hawthorn, Victoria 3122, Australia

³Department of Mechanical Engineering, Imperial College London, Exhibition Road, London SW7 2AZ, UK

⁴Computational Multiphysics Systems Laboratory, Code 6394, Center for Materials Physics and Technology, US Naval Research Laboratory, Washington, DC 20375, United States.

ABSTRACT

This paper first discusses the accuracy of the standard adhesively-bonded joint design code, the analytical A4EI code, that is used in the aircraft industry to determine the strain-energy density in a complex step-lap joint under shear loads. This joint geometry is then used to assess the ability of the A4EI code, and a standard finite-element analysis, to meet the static design requirement that there be no yield at 115% of the design limit load. Secondly, the analysis tools needed for the durability and damage tolerance (DADT) assessment of adhesively-bonded airframes are discussed. This study demonstrates the significant advantages of now adopting a Harman-Schijve approach, which considers a fatigue crack growth rate approach to DADT assessment. Indeed, this study reveals how very different approaches are required to assess the static versus the DADT performance of adhesively-bonded joints. A key finding is that, if a valid DADT assessment of fatigue crack growth in adhesively-bonded structures is to be performed, then the aspect of the fatigue crack growth data associated with the growth of small cracks in bonded joints needs to be resolved, together with the question of what is the value of the fatigue threshold associated with such small, naturally-occurring cracks in bonded structures.

Keywords: Adhesives, bonded joints, fatigue, CMH-17-3G, MIL-STD-1530D, A4EI, Hartman-Schijve

NOMENCLATURE

a	crack length
a_o	initial crack length
A	constant in the Hartman-Schijve equation
A4EI	a standard analytical A4EI code that is widely used in the aircraft industry
CFRP	carbon-fibre reinforced-plastic
CMG	Composite Materials Handbook
da/dN	rate of fatigue crack growth per cycle
D	intercept in the Hartman-Schijve crack-growth equation
DADT	durability and damage tolerance
DC-10	a McDonnell Douglas civil transport aircraft
DLL	design limit load, which is the maximum load seen in flight
DOFS	double-overlap fatigue-specimen
DUL	design ultimate load, which is the DLL multiplied by a suitable factor. (The DUL is often taken as 150% DLL.)
F_{max}	maximum load applied during the fatigue test
F_{min}	minimum load applied during the fatigue test
F-15	a McDonnell Douglas combat aircraft
F/A-18	a McDonnell Douglas aircraft operated by the RAAF
F-111	a US General Dynamics aircraft operated by the RAAF
FAA	Federal Aviation Administration
FALSTAFF	an industry-standard, combat aircraft flight-load fatigue-cycle spectrum
FCG	fatigue crack growth
FEA	finite-element analysis
FM73	a rubber-toughened epoxy-film adhesive from Cytec, USA
FM300K	a rubber-toughened epoxy-film adhesive from Cytec, USA
G	energy release-rate
G_c	quasi-static value of the fracture energy
G_{max}	maximum value of the applied energy release-rate in the fatigue cycle
G_{min}	minimum value of the applied energy release-rate in the fatigue cycle
ΔG	range of the applied energy release-rate in the fatigue cycle, as defined below: $\Delta G = G_{max} - G_{min}$
$\Delta\sqrt{G}$	range of the applied energy release-rate in the fatigue cycle, as defined below: $\Delta\sqrt{G} = \sqrt{G_{max}} - \sqrt{G_{min}}$
$\Delta\sqrt{G_{th}}$	value of $\Delta\sqrt{G}$ at a value of da/dN of 10^{-10} m/cycle
$\Delta\sqrt{G_{thr}}$	range of the fatigue threshold value of $\Delta\sqrt{G}$, as defined below: $\Delta\sqrt{G_{thr}} = \sqrt{G_{thr.max}} - \sqrt{G_{thr.min}}$
$\sqrt{G_{thr.max}}$	threshold value of $\sqrt{G_{max}}$
$\sqrt{G_{thr.min}}$	threshold value of $\sqrt{G_{min}}$
JSSG	Joint Service Specification Guide

K	stress-intensity factor
K_{max}	maximum value of the applied stress-intensity factor in the fatigue cycle
K_{min}	minimum value of the applied stress-intensity factor in the fatigue cycle
ΔK	range of the applied stress-intensity factor in the fatigue cycle, as defined below: $\Delta K = K_{max} - K_{min}$
MD-11	a McDonnell Douglas civil transport aircraft
n	exponent in the Hartman-Schijve crack-growth equation
N	number of fatigue cycles
NDI	non-destructive inspection
Orion P3C	a US Lockheed aircraft operated by the RAAF
R	correlation coefficient
R	stress ratio ($= F_{min}/F_{max}$)
RAAF	Royal Australian Air Force
Triton	a Northrop Grumman unmanned aerial vehicle in use with the US Navy
US	United States
USAF	United States Air Force
W_e	the value of the adhesive strain-energy density at yield
W_f	the fatigue limit value of the adhesive strain-energy density
I, II	subscripts indicating Mode I (tensile) and Mode II (in-plane shear) loading
I/II	subscript indicating Mixed-Mode I/II loading
$\Delta\kappa$	the Hartman-Schijve crack driving force, see Equation (2)
σ	standard deviation

1. INTRODUCTION

As noted in the US Defence Department's Composite Materials Handbook CMH-17-3G [1,2] the design of bonded joints in airframes is based on a "no growth design" philosophy, and the static design tools are, to a large extent, still based on the analytical computer-code A4EI [3] that was developed as a result of the USAF 'Primary Adhesively Bonded Structure Technology (PABST)' programme [4]. When outlining the design considerations MIL-STD-1530D [5] requires that there be no yielding in the adhesive at the design limit load (DLL). The statement in JSSG-2006 [6] is a little different in that it requires no yielding in the adhesive at 115% DLL. MIL-STD-1530D, JSSG-2006 and CMH-17-3G also require that the static strength of a bonded joint be such that there is no failure at the design ultimate load (DUL), which is often taken as 150% DLL. Until recently, it had been thought that if a joint met the (static) strength criteria, then it would meet the criteria that it would not yield at 115% DLL. (Indeed, the United States Composites Materials Handbook CMH-17-3G does not discuss either the JSSG-2006 requirement that there be no yielding at 115% DLL, or the MIL-STD-1530D requirement that there be no yielding at 100% DLL.) However, [7] gave a number of examples where this assumption was shown to be invalid. In these examples, the hypothesis that designing a bonded

joint such that the joint would not fail at the DUL would ensure that there will be no yielding at 115% DLL, or even 100% DLL, was disproven.

CMH-17-3G [2] first notes that the load-bearing capacity of a joint is uniquely determined by the peak value of the strain-energy density in the adhesive that is bonding the joint, and then explains that the industry standard for the design of bonded joints involves the use of the analytical A4EI code [3]. Here it should be noted that the ability of A4EI to predict accurately the static strength of complex multi-step lap joints was substantiated in [8]. CMH-17-3G [2] also states that the accuracy of the program A4EI is such that elaborate finite-element analysis (FEA) models are not needed. Unfortunately, no examples to support this statement are given. However, a number of examples that support this statement, for uniaxial applied loads, are given in [7]. In this context it should be noted that [7] also outlined how the FEA approach developed in [9] for symmetric double-overlap joints, which used conventional FEA, can be used to study relatively complex bonded joints. To date, there has been no comparable study into the static strength of bonded joints subjected to either shear loads or combined axial and shear loads. This shortcoming is addressed in the present paper.

Whilst CMH-17-3G [2] discusses how to assess the static load-bearing capacity of bonded joints under uniaxial loads, little attention is paid to either fatigue or durability related issues. Here it should be recalled that the F-111 boron epoxy doubler program revealed that even if a bonded joint meets the static strength requirements it may not necessarily be durable, e.g. possess a sufficient fatigue life [10, 11]. The main outcomes of the F-111 adhesively-bonded doubler programme delineated, in [10], were:

- a) The need to design to the actual (mixed-mode) fatigue threshold(s) associated with the in-flight multi-axial stress states.
- b) The need to be able to conservatively predict fatigue crack growth (FCG) in the adhesive
- c) That small defects in bonded joints can grow during service.

Finding c) suggests that the small-crack anomaly observed in metallic structures may also be associated with adhesively-bonded joints. (The definition of, and a brief discussion on, small cracks is given in the Appendix.) Indeed, [7] subsequently gave a number of cases where small cracks in adhesively bonded joints/doublers have been observed to grow in full-scale fatigue tests, operational aircraft and in laboratory tests. The need to account for crack growth in operational aircraft is particularly important for aircraft designed to a “no growth” design criteria, such as the F/A-18 Hornet, Super Hornet, F-15, Triton, etc., since crack growth in the adhesive invalidates the

certification basis of the airframe so that the continued operation of such aircraft requires an estimation of the necessary inspection intervals. Thus, for a sound durability and damage tolerance (DADT) methodology, a different and new approach is required to that offered by the static analyses of the AE4I or FEA codes. Such a DADT approach is invariably based upon a fracture-mechanics analysis, which involves determining and modelling the FCG rate.

The observation that small cracks in the adhesive layer, or at the adhesive-substrate interface, can nucleate and grow in bonded joints subjected to operational flight fatigue-loads is also consistent with the DADT methodology outlined in the USAF Durability Design Handbook [12], which is implemented in the USAF approach to assessing the risk of failure [13], as well as in the summary paper on thirty-five years of operating the RAAF F/A-18 aircraft [14], where it is stated that crack growth in metallic airframes essentially starts from the day an aircraft enters operational service. This conclusion can also be found in the USAF Damage Tolerant Design Handbook [15]. Now, it is anticipated that the quality control associated with conventionally manufactured aerospace metallic structures is likely to be as least as stringent, if not more so, than that associated with adhesively-bonded structures and as, discussed above, small cracks have indeed been found to both initiate and grow in bonded joints. Therefore, it is reasonable to assume that the fatigue threshold associated with small naturally-occurring cracking in adhesively-bonded structures will also be very low. By this is meant that the fatigue threshold associated with small naturally-occurring cracks in adhesively-bonded structures may be much lower than that seen in tests performed on specimens with long (artificially-induced) cracks in the adhesive. Thus, from the above, the following statements may be made:

- a) A slow crack growth approach to certifying bonded structures is contained in both JSSG-2006 [6], MIL-STD-1530D [5] and the FAA Advisory Circular 20-107B [16].
- b) The MIL-STD 1530D [5] notes that the design and certification approval requires the use of analytical tools that are capable of modelling crack growth.
- c) The role of testing is to validate, or correct, the damage tolerance analysis.
- d) The NASA Fracture Control handbook NASA HDBK-5010 [17] requires ‘upper-bound’ FCG curves to be used in design.

Hence, it is clear that a methodology for determining the ‘worst-case’, i.e. ‘upper-bound’, FCG curve for structural adhesives is an important requirement (a) for developing conservative designs and (b) for assessing the life and inspection intervals of adhesively-bonded structures. However, the

certification process is complicated by the fact that the DADT assessment of aircraft structures relies on ‘similitude’ [15]. For adhesively-bonded joints it had long been thought that the similitude principle of “Two different cracks growing in identical materials with the same value of $\Delta\sqrt{G}$, and the same $\sqrt{G}_{max}$, will grow at the same FCG rate” held. (Here $\Delta\sqrt{G}$ is the range of the applied energy release-rate in the fatigue cycle, as defined by $\Delta\sqrt{G} = \sqrt{G_{max}} - \sqrt{G_{min}}$; and G_{max} and G_{min} are the maximum and minimum values of the applied energy release-rate in the fatigue cycle, respectively.) However, as shown in [18] this statement is not true. Fortunately, in the examples studied in [18], it was found that the following similitude hypothesis held, viz: two different cracks growing in identical materials with the same value of $\Delta\kappa$ will grow at the same FCG rate, i.e. da/dN ; and that the FCG rate could be expressed in the form:

$$\frac{da}{dN} = D[\Delta\kappa]^n \quad (1)$$

Here $\Delta\kappa$ is the Hartman-Schijve crack driving force [7,18-22], viz:

$$\Delta\kappa = \left[\frac{\Delta\sqrt{G_I} - \Delta\sqrt{G_{Ithr}}}{\sqrt{\{1 - \sqrt{G_{Imax}}/\sqrt{A}\}}} \right] \quad (2)$$

where the term $\Delta\sqrt{G_I}$, for Mode I (tensile loading), is defined by:

$$\Delta\sqrt{G_I} = \sqrt{G_{Imax}} - \sqrt{G_{Imin}} \quad (3)$$

In Equation (2), the term $\Delta\sqrt{G_{Ithr}}$ denotes the threshold value of $\Delta\sqrt{G_I}$ below which no significant growth occurs and D , n and A are constants. It should be noted that the threshold term $\Delta\sqrt{G_{Ithr}}$ differs from the ASTM E647-13a defined threshold $\Delta\sqrt{G_{Ith}}$, which is the value of $\Delta\sqrt{G_I}$ at $da/dN = 10^{-10}$ m/cycle. The expression relating these two terms is:

$$\Delta\sqrt{G_{Ithr}} = \Delta\sqrt{G_{Ith}} - \sqrt{\{1 - \sqrt{G_{Imax}}/\sqrt{A}\}} \left[\frac{10^{-10}}{D} \right]^{1/n} \quad (4)$$

For Mixed-mode I/II loading equation (2) becomes:

$$\Delta\kappa = \left[\frac{\Delta\sqrt{G_{I/II}} - \Delta\sqrt{G_{I/IIthr}}}{\sqrt{\{1 - \sqrt{G_{I/IImax}/\sqrt{A}}\}}} \right] \quad (5)$$

where the subscript ‘I/II’ indicates mixed-mode loading, see [19,22].

Equation (1) is a variant of the NASGRO equation [23], and is termed the Hartman-Schijve crack growth equation, that is based on the early work of Hartman and Schijve [24]. It has been found to hold for FCG in a reasonably wide cross-section of adhesives and nano-composites [7,18-22,25-27], polymers [28,29], delamination growth in composites [30-35], as well as crack growth in a wide range of both conventionally manufactured [36-45] and additively-manufactured metals [46-52] and aluminium-alloy castings [53], crack growth in granite [54], and crack growth in cold spray additively-manufactured 316L stainless steel [55]. An important feature of the Hartman and Schijve approach is that, for metals, the growth of both long and small cracks lie on the same da/dN versus $\Delta\kappa$ curve.

Returning to the question of how to perform a durability/economic life assessment of crack growth in adhesively-bonded and composite structures, it should be noted that [7,20,33,35] have proposed a methodology which computes the variability in the fatigue life by allowing for the variability in the fatigue threshold term, $\Delta\sqrt{G_{thr}}$, and the toughness term, A , in the Hartman-Schijve equation. This approach then uses the statistical approach outlined in JSSG-2006, which recommends the use of A-basis allowables. It also reflects that recommended in NASA HDBK-5010 [17] and in [56,57]. Consequently, with respect to the DADT assessment in adhesively-bonded joints, an aim of the present paper is to investigate the variability in the FCG in joints under an industry standard combat-aircraft flight-load spectrum (FALSTAFF), as well as under a flight-load spectrum associated with the wing of a Orion P3C maritime patrol aircraft. (FALSTAFF is an industry-standard combat aircraft flight-load fatigue-cycle spectrum. One FALSTAFF load-block represents 200 airframe flight hours [58].)

2. STATIC ANALYSES OF A MULTI-STEP LAP JOINT

2.1 Methodology

Whilst [7] validated the accuracy of the analytical A4EI code to compute the strain-energy density in the adhesive layer bonding multi-step lap joints subjected to uniaxial loads, by comparison with a finite element analysis (FEA) approach, there has been no equivalent study for a multi-step joint

under shear loads. Consequently, an analysis of the joint studied in [7], see Figure 1, was performed for the case when the joint was subjected to uniformly distributed shear loads, see Figure 1. Two elastic-plastic 3D FEA models were created. One model consisted of 120,000 twenty-noded isoparametric elements and 514,233 nodes and the other had 414,000 twenty-noded isoparametric elements and 1,728,582 nodes, see Figures 2 and 3. As per [7], the substrates were assumed to be a titanium alloy (Ti-6Al-4V) with a Young's modulus of 110,316 MPa and a Poisson's ratio of 0.3, whilst the epoxy-film adhesive was taken to be FM300K, from Cytec USA, with a shear modulus of 393 MPa and a Poisson's ratio of 0.3. It was also assumed that, as in [7], the adhesive exhibited an elastic-perfectly plastic behaviour with a shear yield stress of 39 MPa and a maximum shear strain to failure of 0.31. This gives the value of the strain-energy density at yield, W_e , to be 1.93 MJ/m³ and a value at failure of 87.8 MJ/m³.

For validation purposes, elastic analyses for a remote uniform shear load per unit width (of bonded joint) of 762 N/mm were performed first. These elastic analyses revealed that the difference in the maximum value of the strain-energy density computed by the two different numerical FEA models was less than 2%. It was also found that the value of the strain-energy density predicted using the analytical A4EI approach was in reasonable agreement with the values obtained using the 3D FEA approaches, see Table 1, with the average of the two 3D FEA results being approximately 7.8% smaller than that obtained using the A4EI method. As such, both the FEA and A4EI approaches could be used to determine the load corresponding to the JSSG-2006 requirement that there be no yield at 115% DLL.

Next, taking the value of the strain-energy density at yield, W_e , to be the measured value of 1.93 MJ/m³, then the load per unit width to reach this value of W_e is estimated to be approximately 560 N/mm, using the analytical AE4I or the 3D FEA elastic codes. Now, assuming that this load corresponds to 100% DLL, we obtain a load per unit width of 840 N/mm at 150% DLL, i.e. which is equivalent to the value of the DUL. For comparison purposes, let us now calculate the failure loads per unit width as predicted using both the 3D FEA and the analytical AE4I elastic-plastic codes, taking in both cases the failure criterion to be the value of the strain-energy density at failure of 87.8 MJ/m³, as above. The corresponding predicted failure loads per unit width are given in Table 2 and were found to be 1177 N/mm and 1528 N/mm, respectively. Thus, as is for the case in [7], with only a uniaxial load per unit width being applied, the design requirement that there be no yield at 100%, or even at 115% DLL, means that the joint would not fail at 150% DLL, i.e. at the DUL. It should also be noted that whilst both the FEA and the A4EI approaches gave similar values of W_e (see Table 1) the failure load per unit width estimated using the A4EI code was about 20% greater than those

determined using the FEA method (see Table 2).

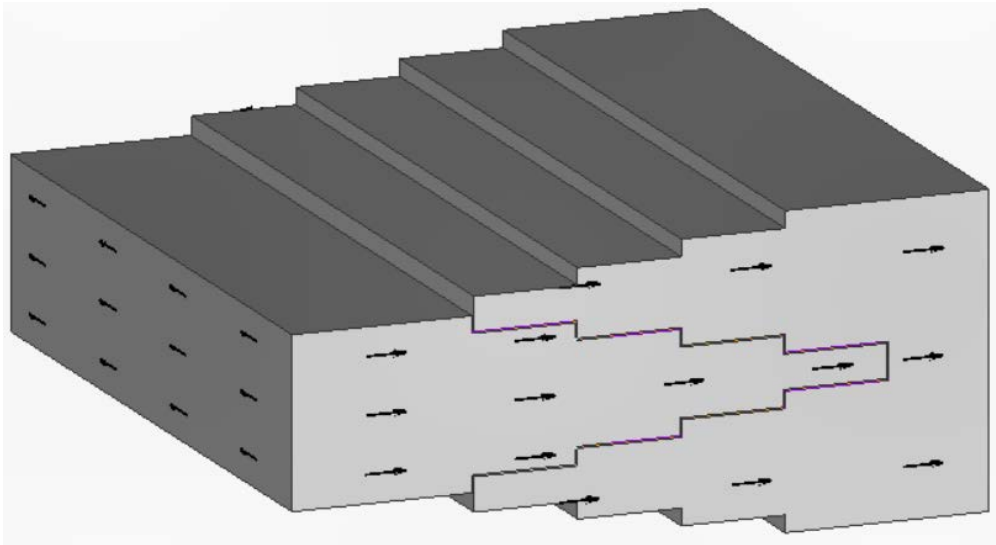


Figure 1. Schematic diagram of the geometry of the multi-step bonded joint subjected to uniformly distributed shear loads per unit width.

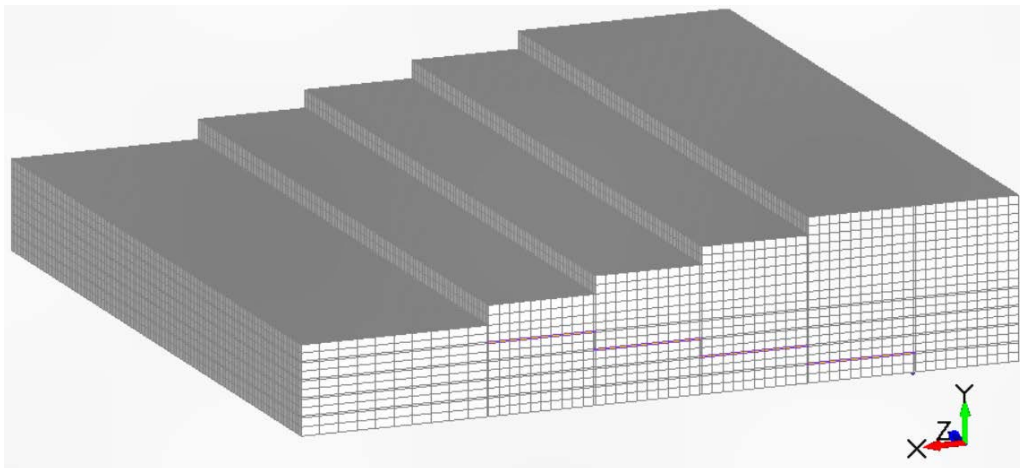


Figure 2. The 3D FEA coarse mesh used for the multi-step bonded joint.

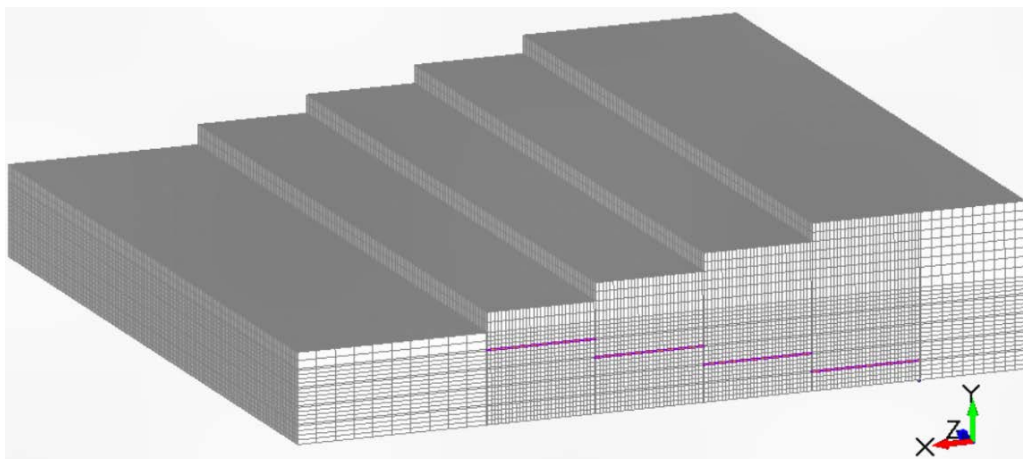


Figure 3. The 3D FEA fine mesh used for the multi-step bonded joint.

Table 1. Predicted strain-energy density for the multi-step bonded joint subjected to a uniformly distributed shear load per unit width of 762 N/mm.

	Average FEA	A4EI	% Difference
Strain-energy density (MJ/m ³)	3.57	3.87	7.8

Table 2. Predicted failure loads per unit width for the multi-step bonded joint subjected to uniformly distributed shear loads.

	Average FEA	A4EI	% Difference
Failure load per unit width (N/mm)	1177	1528	21

2.2 Combined axial and shear loads

Having seen that the analytical A4EI approach may be used to calculate accurately the elastic strain-energy density under simple statically applied shear loads, see Table 1, let us next consider the case of combined axial and shear loads. This is an important consideration. Now, there have been instances where the inner-wing fold step-lap joint in the US Navy F/A-18 Hornet aircraft has been found to contain disbonds [59]. The wing, which acts as a wing torsion box, sees a combination of both axial and shear loads. In this instance the shear loads are approximately one-third of the axial load.

Since we have previously found that the use of the relatively simplistic, analytical A4EI code underestimates the failure load per unit width, this next study firstly only investigates the ability of the AE4I code to determine the elastic strain-energy density of the adhesive layer in the joint. In [7] the joint was subjected to an axial load per unit width of 1048 N/mm. Thus, noting that, in the case of the F/A-18 step-lap joint, the shear loads per unit width are approximately one-third of the axial loads per unit width, let us analyse the case of an axial load per unit width of 1048 N/mm and a shear load per unit width of 350 N/mm, see Figure 4.

The maximum value of the (elastic) strain-energy density computed in this analysis from the FEA approach is presented in Table 3, where it is compared to the corresponding value obtained using A4EI. Here we see that, for the combined axial and shear loads, the use of the analytical A4EI code reasonably accurately predicts the strain-energy density at the worst point in the joint, and is in good agreement with the prediction obtained from the FEA approach. As such, both the FEA and A4EI elastic approaches could be used to determine the load corresponding to the JSSG-2206 requirement that there be no yield at 115% DLL

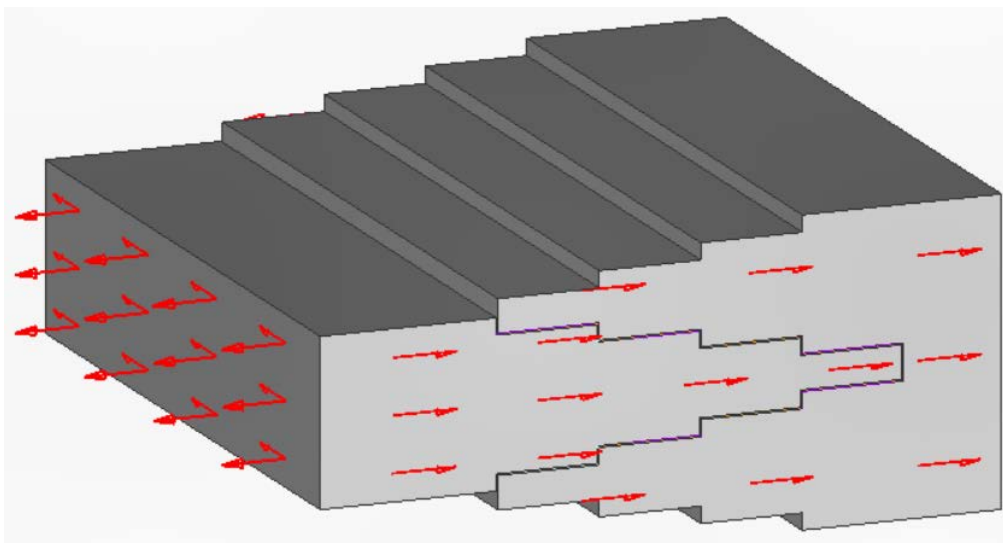


Figure 4. Schematic diagram of the geometry and the combined axial and shear loads per unit width applied to the multi-step bonded joint.

Table 3. Comparison of the computed strain-energy density using FEA with that estimated using A4EI for elastic analyses, for combined axial and shear loads, for the multi-step bonded joint.

	FEA	A4EI	% Difference
Strain-energy density (MJ/m ³)	2.25	2.34	4

Unfortunately, the A4EI code does not allow for combined loads in an elastic-plastic analysis. Whilst there are several ‘in-house’ approaches that attempt to alleviate this shortcoming there is no publicly available documentation or validation of these approaches. Consequently, it is not possible to compare directly the failure loads predicted using FEA with those predicted using the A4EI code. As a result, the best that can be done to evaluate the effect of the combined load state is to compare the difference between the predicted failure loads predicted using the AE4I code for the case of a

uniaxially applied tensile load, this value is taken from [7], with that for the combined load case predicted using an FEA approach. This is done in in Table 4 where it may be seen that the effect of the shear load is to lower the predicted strength of the joint by approximately 20%. This finding clearly illustrates the need to allow for the combined load states.

Table 4. Difference between the calculated failure loads per unit width from combined uniaxial tensile and shear loads using FEA versus only an applied uniaxial-tensile load estimated using A4EI for the multi-step bonded joint. (Here the term failure load refers to the value of the axial load at failure, the value of the corresponding shear load is one-third of the axial load.)

	FEA (combined uniaxial tensile and shear loads)	A4EI (uniaxially applied tensile load only)	% Difference
Failure load per unit width (N/mm)	1501	1872	20

2.3 Implications for designing against fatigue

The PABST program [4] recommended that to avoid fatigue and durability issues the maximum strain-density in a joint should be kept beneath a limiting value, which we will define as W_f . As a result of the F-111 aircraft program, which led to the realisation that designing to static allowables did not ensure that disbonding would not arise in service aircraft [60], then [61,62] introduced a variant of the A4EI approach for computing the value of the load per unit width at which this limiting value is obtained. Application and validation of this “extended PABST” approach for bonded doublers on Boeing MD-11 and DC-10 civil transport aircraft are given in [63,64]. Such approaches are equivalent to the CMH-17-3G “no crack growth” design approach, which stipulates that small naturally-occurring defects must not nucleate or grow during the service-life of a adhesively-bonded structure. Unfortunately, these approaches can lead to over-designed and relatively high-weight structures. Furthermore, they cannot be used to determine the inspection intervals that are needed if, despite the fact the joint was designed in accordance with a “no growth” philosophy, a crack is found in an operational aircraft. In such cases the airframe must either be repaired, replaced, or an alternative approach to living with the damage developed. To this end, the next section discusses an alternative fracture-mechanics based approach for the DADT assessment of adhesively-bonded joints.

3. THE EFFECT OF VARIABILITY OF THE THRESHOLD TERM, $\sqrt{G_{Ithr}}$, ON THE PREDICTED FATIGUE LIFE

3.1 DADT assessment using the Hartman-Schijve methodology

Whilst the previous section has introduced the concept of designing adhesively-bonded joints whereby static strength and durability issues are based on a “no crack growth” approach, experience has shown [7], when subjected to operational flight loads, disbonding and disbond growth can occur in bonded structures. This raises the question of how can we compute and establish the operational life of an adhesively-bonded joint with a disbond. Furthermore, since coupon tests are frequently used to investigate the effect of damage on the remaining life, and since it is now known that if you are to use such laboratory tests as a guide to in-service performance the similitude parameter in the test must be the same as that used in the DADT study on the aircraft [18], this section will use the Hartman-Schijve crack growth equation, i.e. Equations (1) and (2), to investigate this question. In this context, it should be noted that the paper by Virkler et al. [65] highlighted the variability in the da/dN versus ΔK curves that can arise for long cracks in metallic structures. This is shown in Figure 5 which presents a number of curves from this study together with the corresponding A-basis (i.e. the mean- 3σ curve, where σ is the standard deviation). (Where ΔK is the range of the applied stress-intensity factor in the fatigue cycle, as defined by $\Delta K = K_{max} - K_{min}$.)

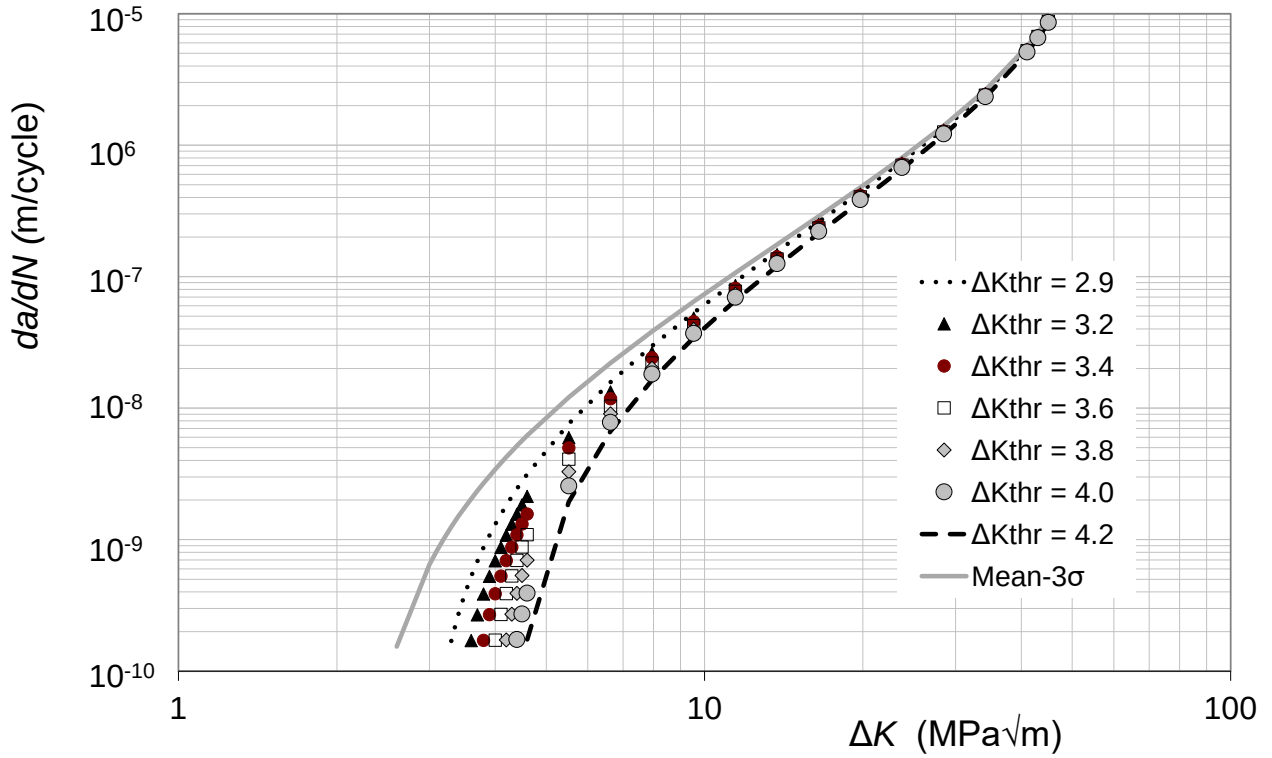


Figure 5. The variability in the da/dN versus ΔK curves given in [65] for the aluminium-alloy AA2024-T3

The variability in the da/dN versus ΔK curves associated with small cracks in metallic structures is generally greater than that for long cracks. An example showing this variability for the aluminium alloy AA7050-T7451 is given in [51]. As such, noting, for adhesives the term $\Delta\sqrt{G}$ is linearly proportional to ΔK , it is reasonable to assume that the variability in the da/N versus $\Delta\sqrt{G}$ data for FCG in an adhesive will be, at least, comparable to that seen in metals.

To the best of the authors knowledge, reference [66] was the first to study the effect of the variability of the threshold term on the FCG in an adhesively-bonded joint. This study focused on the symmetric double-overlap fatigue-specimens (DOFSs) tested by Cheuk and et al. [67]. The right-hand side of the symmetric DOFS is shown schematically in Figure 6 and tensile loads were cyclically applied longitudinally to the double-overlap joints. The width of the specimen was 20 mm. The inner and outer substrates were an aluminium-alloy (AA2024-T3) and were bonded using a rubber-toughened epoxy-film adhesive (FM73 from Cytec, USA). The fatigue crack was observed to grow cohesively through the adhesive layer from the end of the bonded overlap via naturally-occurring defects in the adhesive layer. The inner aluminium-alloy substrate was 400 mm long and 6.4 mm thick. The outer aluminium-alloy substrate was 200 mm long and was 3.05 mm thick. The FM73 epoxy-film adhesive

was 0.4 mm thick. In the experiments reported in [67] the test frequency was 3 Hz and a simple sinusoidal fatigue-cycle spectrum was employed. In [67] the maximum stress in the aluminium-alloy remote from the joint was 195.3 MPa and the R -ratio was 0.0. The modulus and Poisson's ratio associated with the aluminium-alloy substrates and the FM73 adhesive used in [67] are shown in Table 5. The values of the Hartman-Schijve constants used to represent the FM73 adhesive are given in Tables 6 and 7, and the original experimental data used to deduce these constants were reported in [68]. (Whilst the values of the constants used in this study were taken from the open literature [66], it should be noted that for cases when the parameters in the Hartman-Schijve equation are unknown they are best determined using a non-linear optimization scheme. One such non-linear optimization scheme is outlined in [69].)

Table 5. Mechanical properties of the aluminium-alloy and epoxy-film adhesive [67].

Properties	Aluminium 2024-T3	FM73 adhesive
Elastic modulus, E (GPa)	72	2.30
Poisson's ratio, ν	0.33	0.35

Table 6. Values of the Hartman-Schijve constants for the FM73 adhesive used in [66], see Equations (1) and (2).

D (m/cycle)	n	A (J/m ²)	$\Delta\sqrt{G_{Ithr}}$ ($\sqrt{\text{J/m}^2}$)
1.9×10^{-10}	2.7	2000	7.1

Table 7. The mean value and the standard deviation of the term $\Delta\sqrt{G_{Ithr}}$ for the FM73 adhesive given in [66].

Mean ($\sqrt{\text{J/m}^2}$)	7.1
Standard deviation, σ ($\sqrt{\text{J/m}^2}$)	0.5
Mean - 3 standard deviations ($\sqrt{\text{J/m}^2}$)	5.6

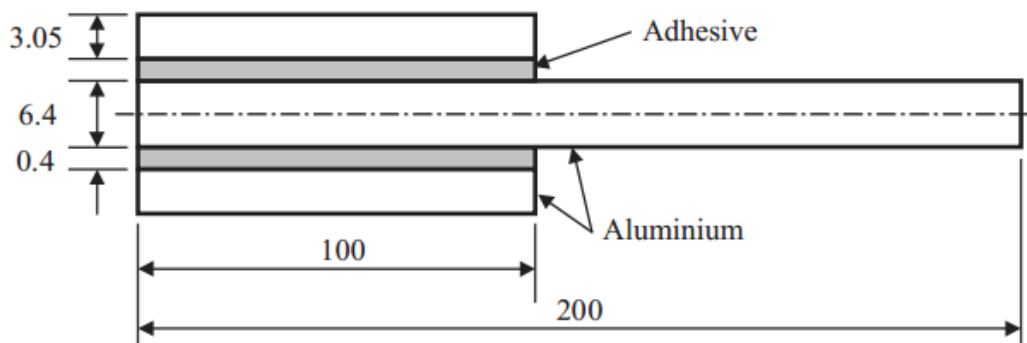


Figure 6. Schematic of the right-hand-side of the symmetric double-overlap fatigue-specimen (DOFS), from [67]. (Tensile loads were cyclically applied longitudinally to the double-overlap joints and the dimensions shown are in mm.)

The relationship between the energy release-rate, G , versus the crack length, a , used in [66] is shown in Figure 7 which presents the curves given in [66,67] for this particular problem.

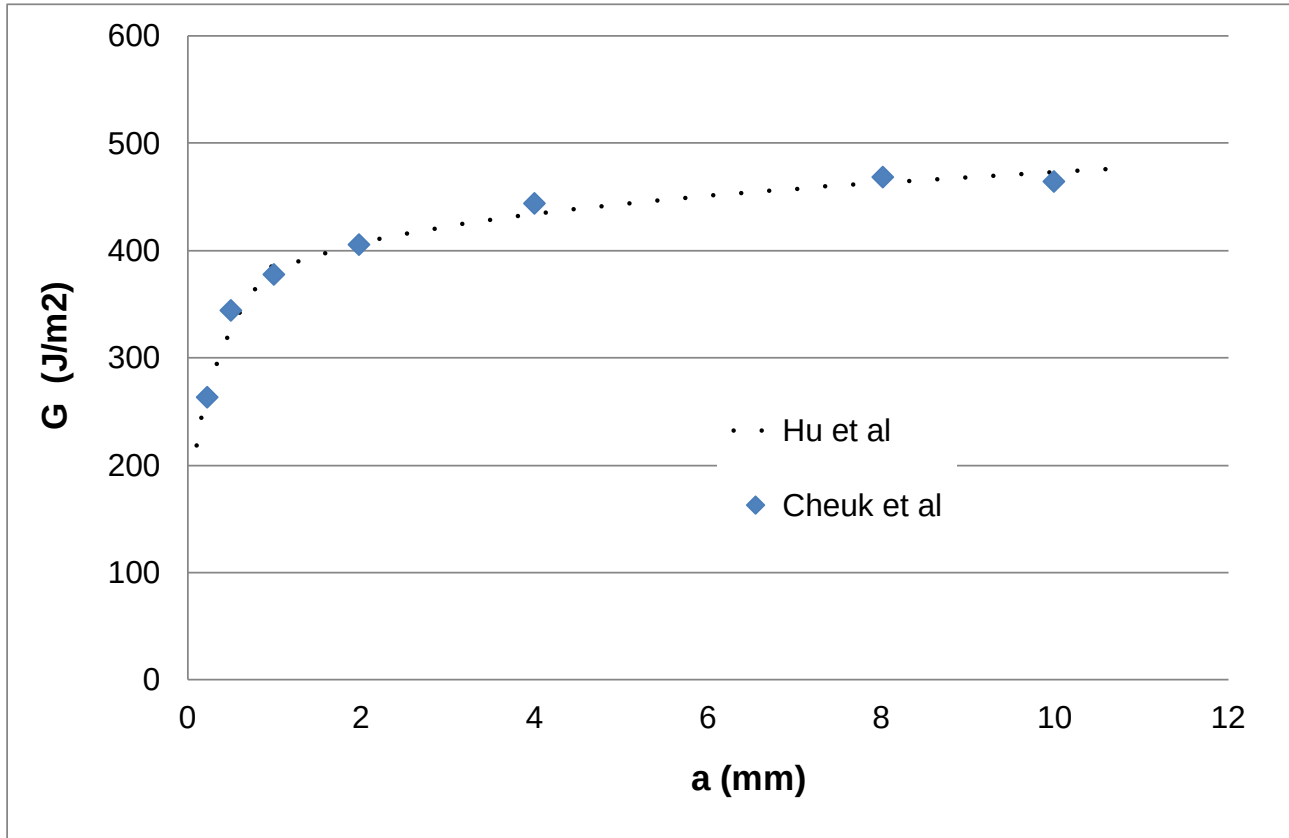


Figure 7. The calculated values of the energy release rate, G , as a function of the crack length, a , for the DOFS. The FEA results from Cheuk et al. [67] and from Hu et al. [66] are both shown.

Reference [66] presented a comparison between the measured and the predicted curves for the growth of a crack, a , versus the number, N , of fatigue cycles using either the ‘mean’ or the ‘mean - 3σ ’ (i.e. the A-basis allowable) values of $\Delta\sqrt{G_{Ithr}}$ given in Table 7. The size of the initial crack length, a_o , used in [66] was taken from [67] to be 0.05 mm. The results of this analysis are shown in Figure 8, where we see that the curve predicted using the mean value of $\Delta\sqrt{G_{Ithr}}$ of $7.1 \sqrt{\text{J/m}^2}$ was in reasonable agreement with the experimental measurements. We also see that the curve predicted by using the ‘mean - 3σ ’ value of $\Delta\sqrt{G_{Ithr}}$ of $5.6 \sqrt{\text{J/m}^2}$ gives a ‘worst-case’, more conservative, prediction which encompasses the variability observed in the experimental data.

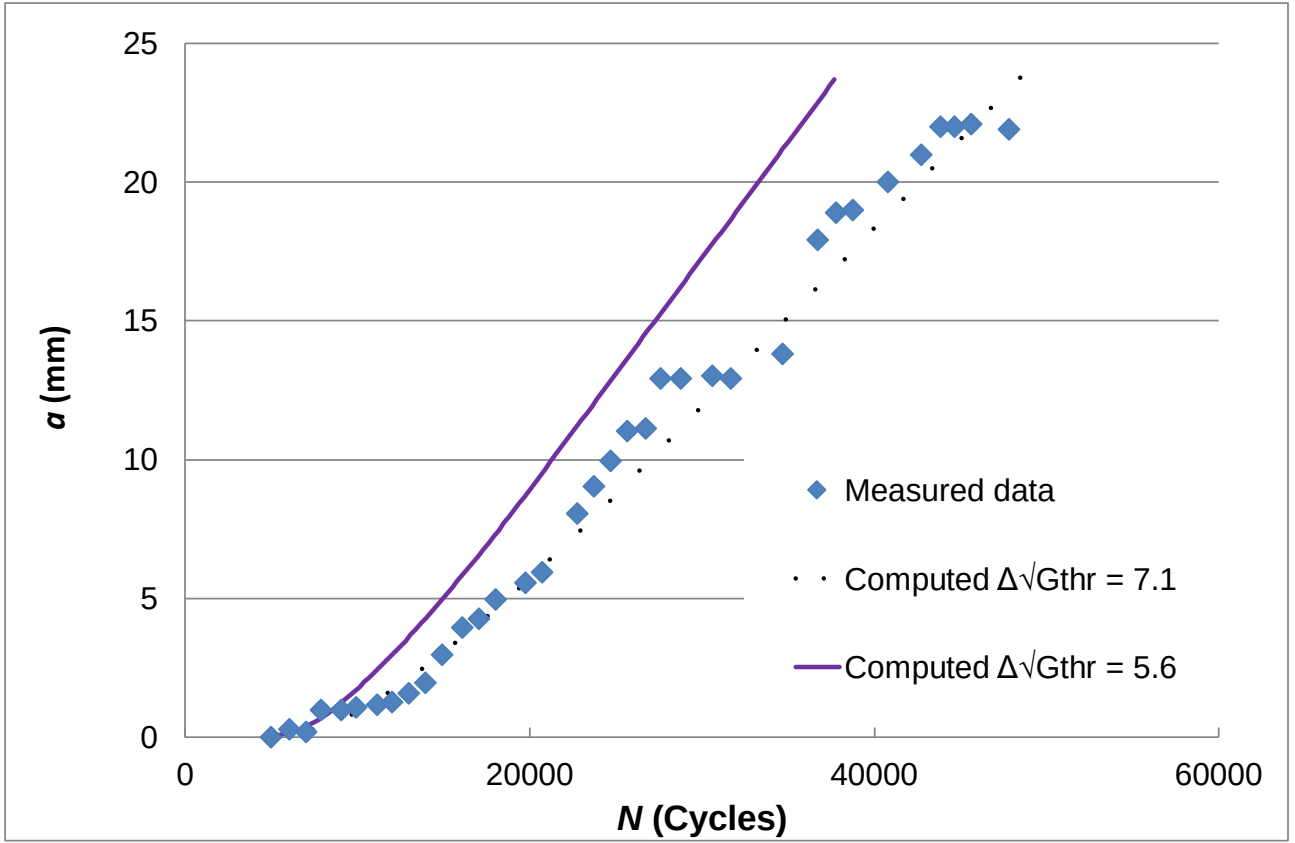


Figure 8. Comparison between the measured and predicted crack length, a , histories as a function of the number, N , of fatigue cycles for the DOFS corresponding to a remote maximum stress of 193.5 MPa under a simple sinusoidal fatigue-cycle spectrum, from [66].

Reference [70] also studied the effect of the variations in the threshold term for the case when the specimen was subjected to a FALSTAFF flight-load spectrum with a maximum remote stress of either 134 MPa or 193.5 MPa. This study assumed that the cracks had nucleated from a small naturally-occurring sub-millimeter material discontinuity and subsequently grew (undetected) to a size of 3 mm, at which stage it was detected. To study the effect of the variability in the fatigue threshold [70] presented the results of analyses performed using both the mean (i.e. $7.1 \sqrt{\text{J/m}^2}$) and the ‘mean - 3σ ’ (i.e. $5.6 \sqrt{\text{J/m}^2}$) values of $\Delta\sqrt{G_{Ithr}}$. The results of this study are reproduced in Figures 9 and 10. (In these Figures it should be noted that one FALSTAFF fatigue load-block corresponds to 200 flights.) This study revealed that, as a result of the large number of small amplitude load-cycles in the FALSTAFF flight-load spectrum, the FCG rate, and hence the service-life of the adhesively-bonded specimen, is a relatively strong function of the value of the fatigue threshold that is employed in the analyses. This finding reinforces the need to determine a statistically-valid value of $\Delta\sqrt{G_{Ithr}}$ for the fatigue threshold, and hence so determine an ‘upper-bound’ FCG rate curve where the variability of the FCG rate for the adhesive joint is considered.

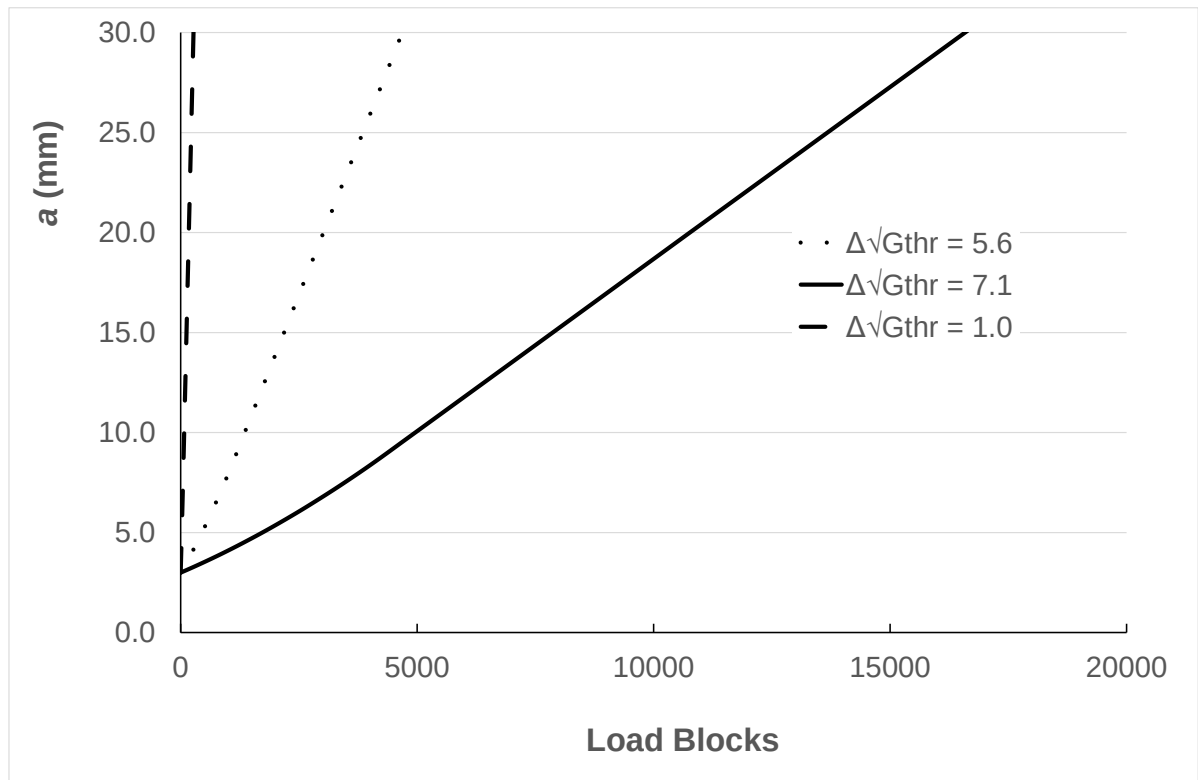


Figure 9. Predicted crack length, a , histories as a function of the number of load blocks for the DOFS corresponding to a remote maximum stress of 134 MPa under the FALSTAFF flight-load fatigue-cycle spectrum. (The initial crack length was taken to be 3 mm.)

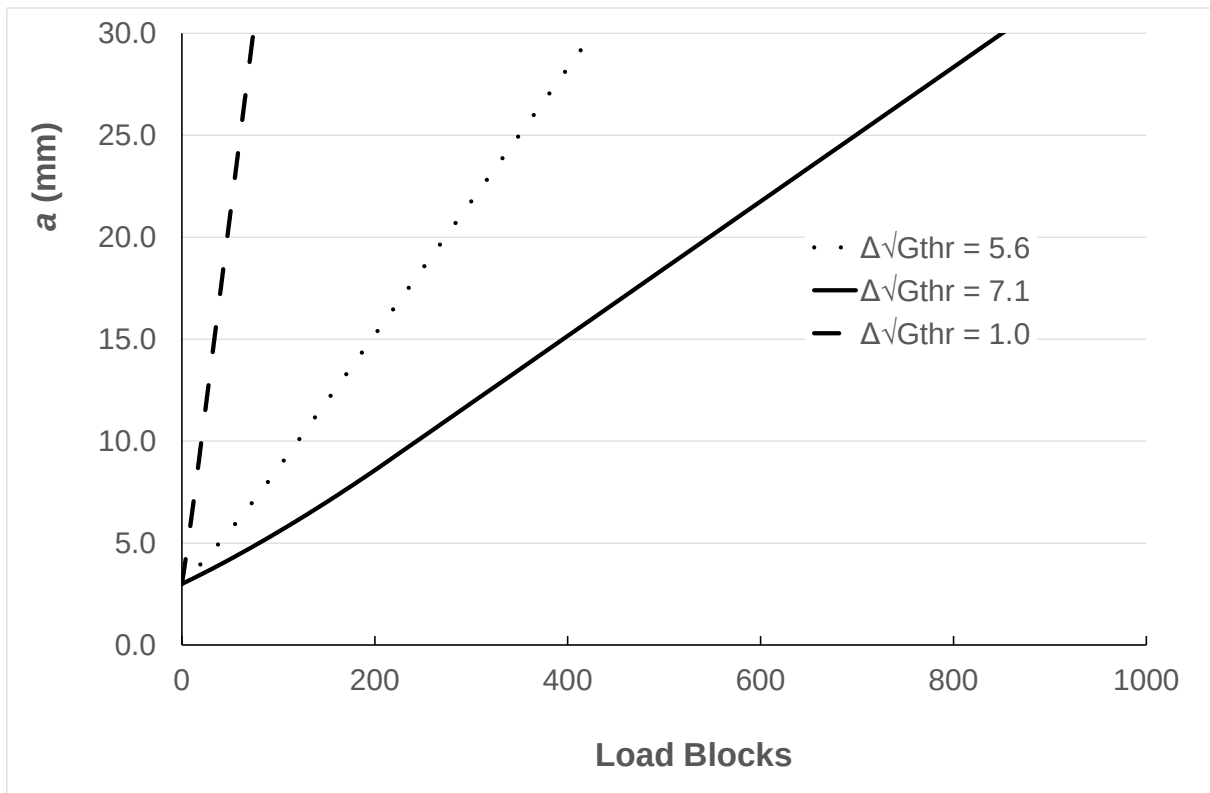


Figure 10. Predicted crack length, a , histories as a function of the number of load blocks for the DOFS corresponding to a remote maximum stress of 193.5 MPa under the FALSTAFF flight-load fatigue-cycle spectrum. (The initial crack length was taken to be 3 mm.)

3.2 Allowing for the variability in the growth of small naturally-occurring cracks

It has previously been mentioned that small naturally-occurring cracks may grow at faster rates in adhesively-bonded specimens and structures than relatively long cracks, since for the relatively small cracks the fatigue threshold can be expected to have a very low value, see Appendix. This raises the question of what would the effect be of setting the threshold term $\Delta\sqrt{G_{Ithr}}$ to a small value, say $1.0 \sqrt{\text{J/m}^2}$, or even $0.1 \sqrt{\text{J/m}^2}$. To examine this question, the analyses shown in Figures 9 and 10, based on our Hartman-Schijve methodology, were repeated with $\Delta\sqrt{G_{Ithr}} = 1.0 \sqrt{\text{J/m}^2}$, and the resultant predicted crack-length histories are added into Figures 9 and 10.

Examining Figures 9 and 10, we see that the computed life is a strong function of the fatigue threshold. It could be argued that FALSTAFF, which is representative of a combat aircraft flight-load spectrum, is dramatically more severe than flight-load spectra representative of other classes of operational aircraft. To investigate this, we next considered the case when the flight-load spectrum that is seen in a RAAF Orion P3C aircraft, at a fatigue critical location FCA 352¹, was used. In this instance, the crack growth associated with $\Delta\sqrt{G_{Ithr}} = 7.1 \sqrt{\text{J/m}^2}$ is very slow, so that Figure 11 only presents the computed crack growth histories associated with $\Delta\sqrt{G_{Ithr}} = 1.0$ and $5.6 \sqrt{\text{J/m}^2}$. (In Figure 11 one load block corresponds to 15,000 flight hours.) Here we again see the same trend, in that there is a large difference in the computed lives associated with the values of $\Delta\sqrt{G_{Ithr}} = 1.0$ or $5.6 \sqrt{\text{J/m}^2}$.

¹ The fatigue-critical location FCA352 corresponds to the fairing-dome nut holes in the lower surface panels at the inboard nacelle [38].

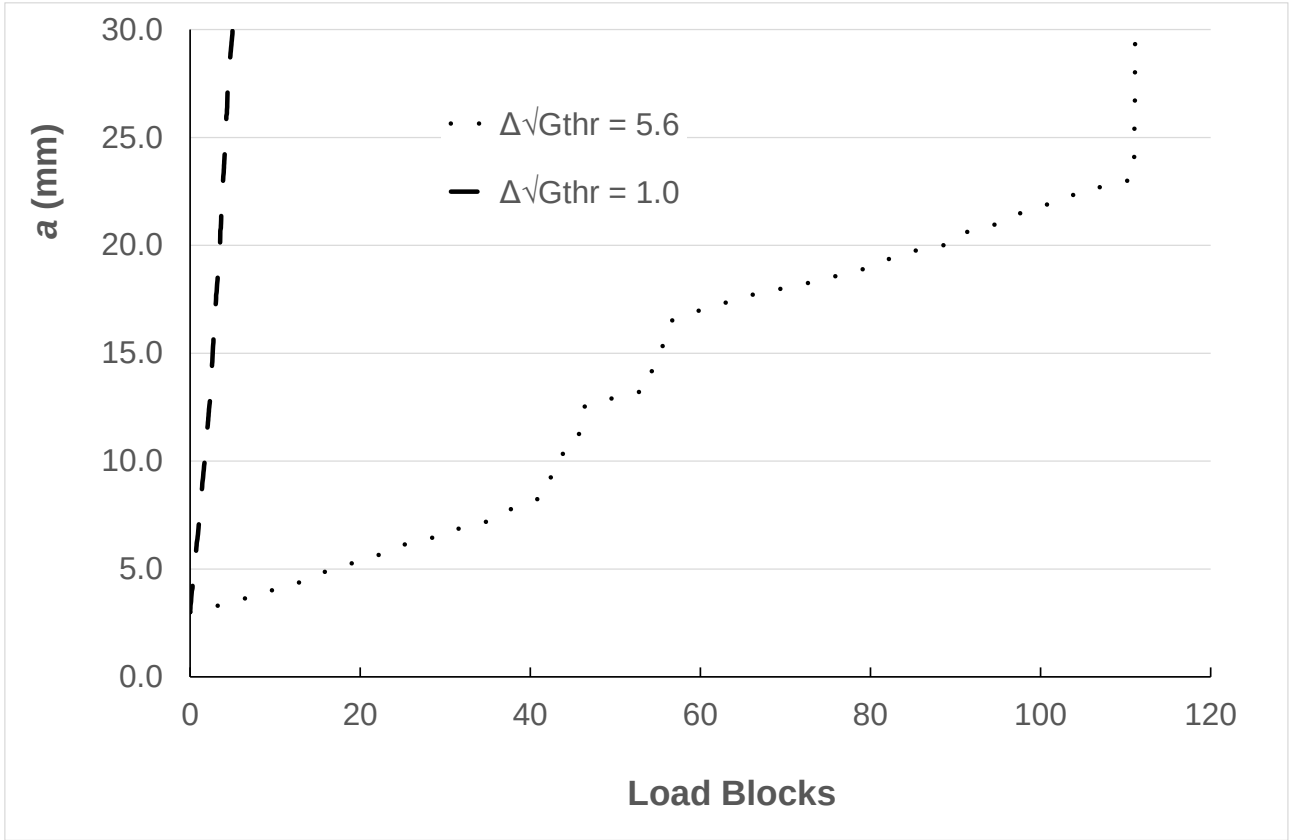


Figure 11. Predicted crack length, a , histories as a function of the number of load blocks for the DOFSs corresponding to a remote maximum stress of 193.5 MPa and a value of $\Delta\sqrt{G_{Ithr}} = 1.0$ and $5.6 \sqrt{\text{J/m}^2}$ under the flight-load spectrum seen at FCA 352 in an Orion P3C aircraft.

Since the value of $\Delta\sqrt{G_{Ithr}} = 1.0 \sqrt{\text{J/m}^2}$ used in the above analyses was somewhat arbitrary, the analysis was repeated using a value of $\Delta\sqrt{G_{Ithr}} = 0.1 \sqrt{\text{J/m}^2}$. The computed crack growth histories are shown in Figure 12. Here we see that further reducing the threshold value of $\Delta\sqrt{G_{Ithr}}$ to $0.1 \sqrt{\text{J/m}^2}$ results in a further significant reduction in the computed fatigue life. Therefore, analyses to determine the DADT of adhesively-bonded structures should ideally be based on the fatigue threshold measured using test specimens with small naturally-occurring defects.

At this point, it should be recalled that a USAF study into how to determine the durability, and hence economic life, of metallic airframes [71] highlighted the need to use FCG curves associated with the growth of small cracks. In this context, we have also seen that the computed lives of a simple adhesively-bonded joint are a strong function of the fatigue threshold. As such, experimental data concerning the FCG associated with the growth of naturally-occurring cracks in bonded joints are needed to resolve the question of what is the fatigue threshold associated with such small naturally-occurring cracks in bonded structures? Unfortunately, this data does not currently exist. Furthermore,

noting the large variability associated with the growth of short/small cracks in metallic structures [47,51], such test data needs to capture this variability for small cracks in adhesively-bonded joints. It is therefore strongly recommended that attention is focused on obtaining the relevant FCG data needed to perform a durability analysis.

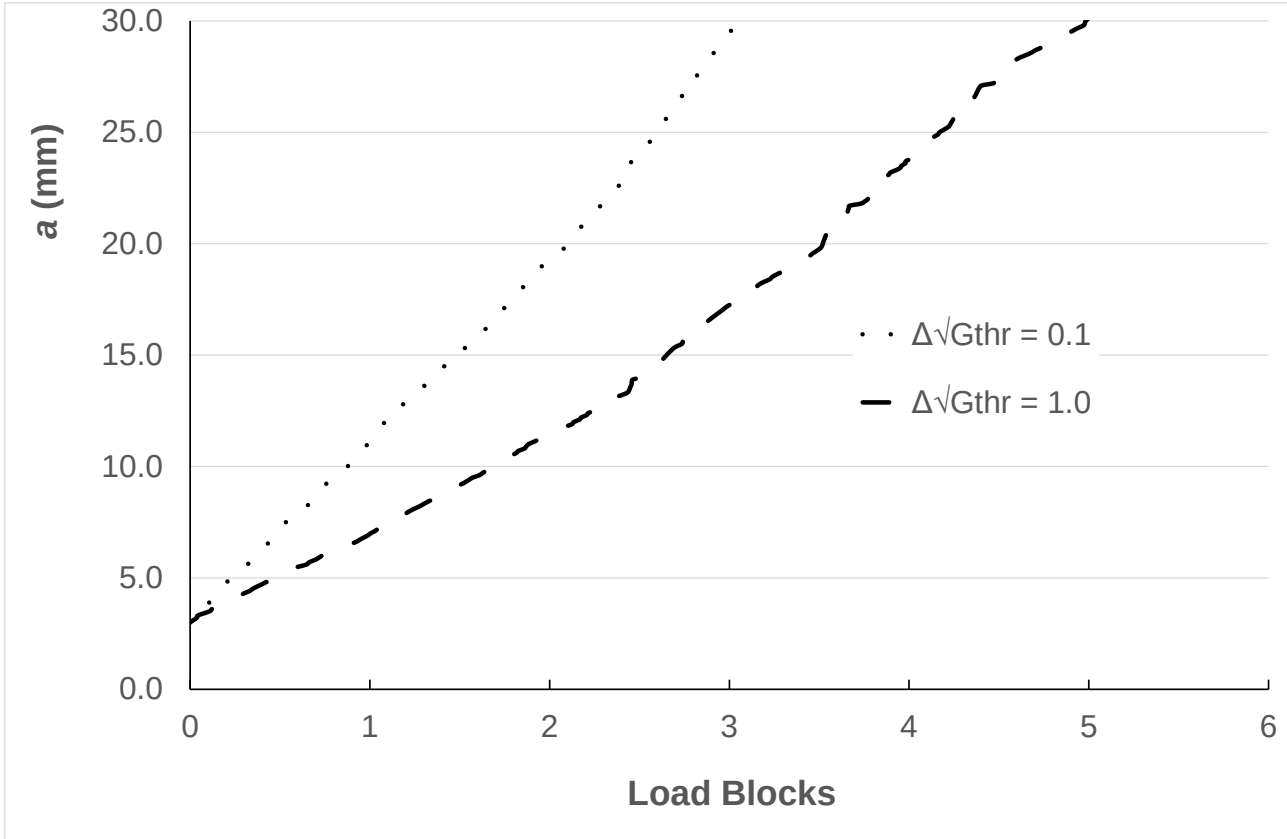


Figure 12. Predicted crack length, a , histories as a function of the number of load blocks for the DOFSs corresponding to a remote maximum stress of 193.5 MPa and a value of term $\Delta\sqrt{G_{Ithr}} = 1.0$ and $0.1 \sqrt{(\text{J}/\text{m}^2)}$ under the flight-load spectrum seen at FCA 352 in an Orion P3C aircraft.

4. CONCLUSIONS

This paper has discussed the analysis tools needed for (a) the calculation of the static design limit load (DLL) and design ultimate load (DUL), and (b) the durability and damage tolerance (DADT) assessment of adhesively-bonded joints. Firstly, it has been shown that, whilst CMH-17-3G discusses the static design of adhesively-bonded joints, it does not discuss how to assess the static performance if the JSSG-2006 requirement that there is no yield in the adhesive at 115% design limit load is to be met. To address this shortcoming, this paper has presented examples that illustrate how to use conventional finite element analysis (FEA) to assess if, under shear loads or combined axial and shear loads, a multi-step lap joint meets the required design requirements. Secondly, however, such static analysis approaches using the analytical AE4I or numerical FEA codes are clearly equivalent to the

“no crack growth” approach outlined in CMH-17-3G, which does not allow for the growth of small, naturally-occurring defects during the service-life of a bonded structure nor suggest meaningful maintenance procedures. Furthermore, such approaches may lead to grossly over-designed and relatively high-weight structures. Therefore, an alternative fracture-mechanics based approach for the DADT assessment of adhesively-bonded joints has been discussed.

In this context attention has been drawn to (a) the need to follow the building-block approach outlined in the Joint Services Structural Guidelines, (b) to use a valid similitude parameter in the DADT analysis, and (c) the need to account for the variability in the computed fatigue life. The importance of allowing for the variability in the measured fatigue crack growth (FCG) rate has been illustrated by considering crack growth in adhesively-bonded double-overlap joints subjected to (a) an industry-standard, combat-aircraft flight-load fatigue-cycle spectrum (i.e. FALSTAFF) and (b) a Orion P3C flight-load spectrum. This study has revealed that the computed fatigue lives are a strong function of the assumed fatigue threshold. As such, if a valid DADT assessment of FCG in bonded structures is to be performed then experimental data for FCG associated with the growth of small, naturally-occurring cracks in adhesively-bonded joints is needed to resolve the question of: *‘What is the fatigue threshold associated with such small, naturally-occurring cracks in adhesively-bonded structures?’*

Unfortunately, this data does not currently exist. In this context, the large variability associated with the growth of short cracks in metallic structures, means that tests performed to determine the FCG curves associated with small cracks for bonded joints also needs to capture the variability that is likely to be associated with the growth of small naturally-occurring cracks in adhesively-bonded structures.

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APPENDIX

In the ASTM fatigue test standard ASTM E637-13a [72] cracks are defined as being small when (a) their length is small compared to relevant microstructural dimension, (b) their length is small compared to the scale of local plasticity (a linear-elastic fracture-mechanics limitation), or (c) they are what E647 terms as ‘physically small’, i.e. less than 1 mm. This last condition is particularly

relevant to airworthiness certification since, for metallic structures, MIL-STD-1530D [5] and JSSG-2006 [6] require that the durability assessment of the structure should use a maximum equivalent initial damage size (EIDS) of 0.01 inch (0.254 mm). In this context it should be noted that the NASA Handbook NASA-HDBK-5010 [17] requires that the worst case crack growth curve be used and that [17] has shown that for cracks in the aluminium alloy AA7050-T7451, with sizes of the order of 0.254 mm, the worst case fatigue threshold is very small and is similar to that seen for micrometer sized cracks. This finding is consistent with the statement in Appendix X3 of E647-13a [72]: “It is not clear if a measurable threshold exists for the growth of small fatigue cracks”. In this context it should be noted that experience has shown that, when computing the growth of small naturally-occurring cracks in a range of metallic materials under several different combat aircraft flight load spectra [73]; civil aircraft (miniTWIST) spectra [41]; maritime reconnaissance flight load spectra [38], etc., accurate predictions of the crack length histories are obtained without the need to account for this effect.

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