EUROPEAN FITNESS-FOR-SERVICE PROCEDURE FITNET AND ITS

APPLICATION TO WELDED AEROSPACE STRUCTURES

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ABSTRACT

The FITNET fitness-for-service (FFS) procedure is newly developed to provide rules for the design, fabrication, and flaw (or degradation during the service) assessment aspects of the metallic load-bearing structures with or without welds. The FITNET FFS Procedure provides assessment rules for flaws or damage due to fracture, fatigue, creep and corrosion to demonstrate the structural integrity of the component.

This paper presents validation examples of the fracture module of the FITNET FFS procedure using thin-walled panels with laser beam welded (LBW) stiffeners. The panels were made of high strength aluminum alloys Al6013-T6 and Al6156-T6. These structure-like laboratory specimens were used to investigate the residual strength behaviour of the fuselage components within the framework of the damage tolerance analysis. The stiffened panels contained two, three and four welded stringers. The stringers oriented in loading direction and panels contained a single center crack artificially introduced between the stringers into the skin material. Additionally, some of the four-stringer panels contained welded clips (short T-joint attachments) located between the stringers. The experimental results of the panels were compared with the predictions of the FITNET FFS procedure. Hence, this paper focuses on the use of this new procedure or the predictions of the critical conditions of these complex thin-walled panels.

The analysis results have shown that the FITNET FFS fracture module can successfully be used for the prediction of the residual strength behaviour of the thin-walled airframe components.

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INTRODUCTION

The aircraft fuselage structures are predominantly thin-walled metallic components and damage tolerance behaviours are stringently assessed for postulated or real flaws for fatigue and fracture. Damage tolerance evaluation procedures are partly used to develop optimized inspection and maintenance guidelines for existing structures to operate safely. Furthermore, it provides technically sound remaining life predictions to enhance economic life of the structures while ensuring the safety. Commonly used structural integrity procedures [1-7] are the techniques used to demonstrate the fitness-for-service of engineering components (with and without welds) transmitting loads. FFS procedures have important implications for economic development in assuring the quality of engineered goods and services. Used correctly, they can increase efficiency by preventing over design and unnecessary inspection and repair. Additionally, they can provide a balance between economy and concern for individual safety and environmental protection, where these are affected by component failure. They have been developed and validated using cases of the general engineering structures rather than thin-walled airframe components. However, FFS procedures such as SINTAP [4, 5] and FITNET [8] are of value at the design stage to provide assurance for new constructions, particularly where these may be innovative in the choice of materials or fabrication methods, and at the operation stage to provide assurance throughout the life of the airframe structures.

The application of FFS procedure during the pre-development or design stage of a new aircraft component using a new material or fabrication method (such as welding instead of riveting) can provide significant help for prediction of allowable stresses for a given material, fabrication method and flaw size. Evaluation of the strength and fracture behaviours of the flat technological panels with structural details is of great value, if experimental findings can be predicted using engineering flaw assessment procedures such as FITNET. However, there is a lack

of practice and information on the application of FFS procedures for the predictions of the residual strength behaviours of the thin-walled, in particularly, stiffened panels or components.

For fuselage structure the conventional procedure for the residual strength analysis is based on stress intensity factor solutions and fracture toughness data and this is generally being considered as sufficient for a single damage (or crack) analysis. The damage types that are considered for the damage tolerance analysis are schematically given in **Figure 1**.The principal outlines of this analysis [9] are given in the Case Studies and Tutorial Section (Vol. II, Section 14.42) of the FITNET FFS Procedure. It is being mentioned in that section that the current regulation (Damage tolerance and fatigue evaluation of structure, FAR 25.571 Amendment 96) requires clearly a damage tolerance analysis for the damage tolerant structure. Furthermore, based on these evaluations, inspections or other procedures must be established, as necessary, to prevent catastrophic failure. Basically, the damage tolerance evaluation comprises a residual strength analysis with the result of the critical crack length and the crack growth analysis providing the crack growth curve between an initial flaw or the detectable crack length and the critical crack length. The residual strength analysis is based on the static limit loads, which are the loads occurring maximum once per life. The purpose of the residual strength analysis is to determine the maximum allowable crack length a_c (last point of stability), which corresponds to the static limit stress $\sigma_{c,l}$ as required by FAR 25.571. This allowable crack length is also called maximum tolerable crack length or critical crack length. In addition to this existing procedure, the FITNET type of analysis could be used to predict the residual strength of the thin-walled cracked components.
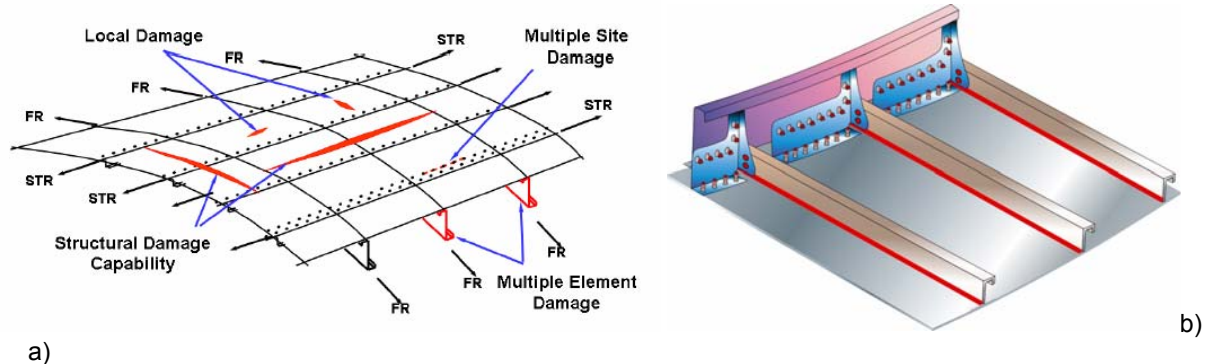

Figure 1: a) Damage types to be considered during damage tolerance evaluation – fuselage examples [9]
b) Laser beam welded skin-stringer panel with riveted skin-clip joints

Recently, an investigation [10, 11] was conducted to analyse the residual strengths of the flat Al-alloy panels with laser beam and friction stir welds both in butt-joint and laser beam welded stiffened panel forms. These investigations used the homogeneous (Option 1, 3) and strength mis-match Options (Option 2) as described in the SINTAP procedure [4, 5] and also in the FITNET FFS [12] to predict the residual strength of the strength undermatched welds. It should be mentioned that both laser beam and friction stir welding of 6xxx series Al-alloys produce lower strength weld regions (having mis-match factor, M, is higher than 10%) than the base metals. This situation can be taken into account by using strength mis-match option (Option 2 or Option 3-mismatch) of the FITNET FFS fracture module, if crack tip deformation behaviour is significantly affected by this undermatching. However, the strength mis-matching option is usually being used, if the crack to be analysed is located fully or partially within the weld region and stress/strain field of its crack tips are significantly affected by the strength heterogeneity of the weld. Therefore, a cracked component where a crack stays perpendicular to the strength mis-matched weld seam can be analysed without using strength mis-match options of the FITNET FFS procedure.

Therefore, the purpose of the present investigation is to analyse further cases of aerospace structures to apply FITNET FFS fracture module to predict residual strengths of the welded (integral) large flat panels with complex geometries. The flat panels considered in this study contain local damage crack types (perpendicular to the stringers) and structural damage type of crack with broken stringer (STR) are shown in **Figure 1a** and refers to structural damage capability of laser beam welded stringers**, Figure 1b.**. By doing such an analysis, it is aimed to provide a clear guidance on the application steps of the FITNET FFS procedure and its verification.

EXPERIMENTAL PROCEDURE

The tensile properties of the Al-alloys were determined from standard flat tensile specimens. The fracture resistance (R-curves) of the materials and weld joints was determined by testing of C(T)50 type fracture toughness specimens having width of 50 mm (W=50 mm, $a_0/W=0.5$) and thickness of respective material.

a)   b)   c)   d)
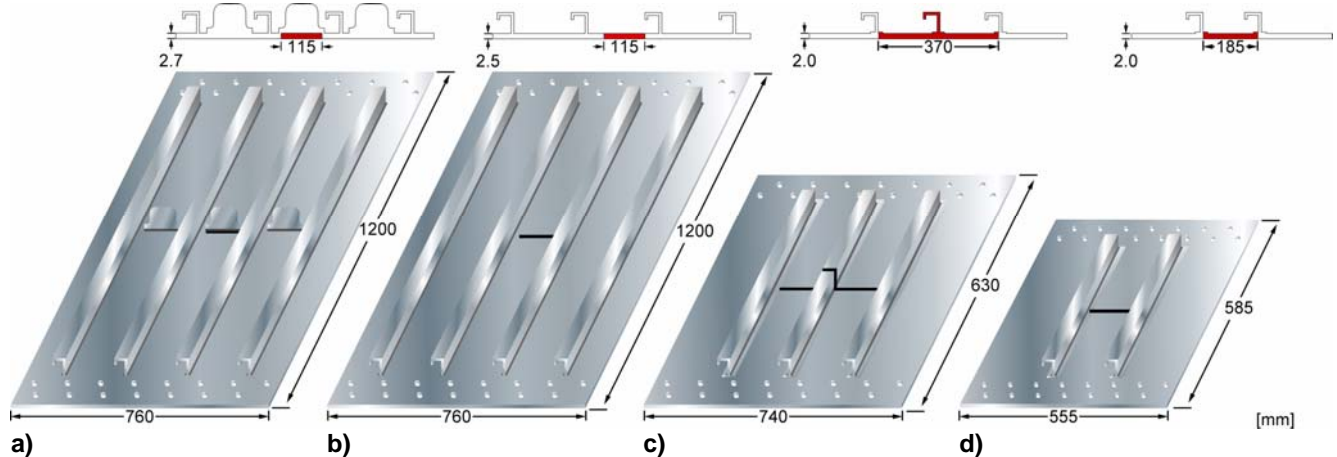
Figure 2: Schematic representations of the test panels; a) Four stringer panel with three transverse clips with the notch at the weld toe of the central clip b) Four stringer panel with skin crack, c) Three stringer panel with broken central stiffener (two bay crack), d) Two stringer panel with one-bay crack.


a)   b)
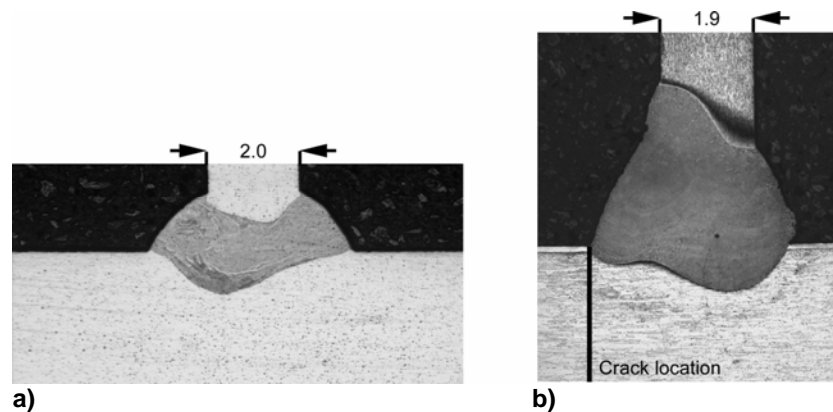
Figure 3: Cross-sectional views of the laser welded T-joints; a) skin-stringer and b) skin-clip joints.

The panels investigated in this study were stiffened by LB welded stringers, **Figure 2**. The four-stringer panels were made of a new generation high strength Al-alloy, Al6156 (**Figure 2a, 2b**). In case of two- and three- stringer panels (**Fig. 2c and 2d**), the material was Al6013, which is very similar to the Al6156 grade. Both alloys are considered as weldable for integral airframe applications. For both material types, the panels were welded at T4 heat treatment condition and then post weld heat treated to T6 temper. The widths of the panels were varied in the range of 555 mm to 760 mm, depending on the panel geometry, **Figure 2**. The length of the panels was in the range of 585 mm to 1200 mm and orientation of welded stringers was parallel to the loading axis of the panels. The skin thicknesses of the panels were 2.0 to 2.7 mm with similar welded stringer thicknesses (**Figures 2-3**).

The **Figure 3** is showing the cross sections of the weld joints for the skin-stringer (T-joint is welded from both sides) and skin-clip (welded from one side) joints. Both weld joints exhibit strength undermatching but this not necessarily should be taken into account during the residual strength analysis of these panels due to the lack of significant interaction between crack tip and weld seam. The notch is placed in all cases perpendicular to the

stringers with varying distance to the stringer weld seams (**Figure 2**). In all panels the initial through thickness notch was introduced by electrical discharge machining and then extended by further 2 to 3 mm by cyclic loading in order obtain a sharp fatigue crack tips at the centers of these M(T) panels. In case of the two-stringer panel (**Figure 2d**), the initial total crack length was so-called "one-bay" (=distance between two stringers) long and yielded to a ratio of $a_0/W=0.33$. In case of the three-stringer panel (**Figure 2c**), the crack was "two-bays" long with a broken central stringer ($a_0/W=0.50$).

a)   b)
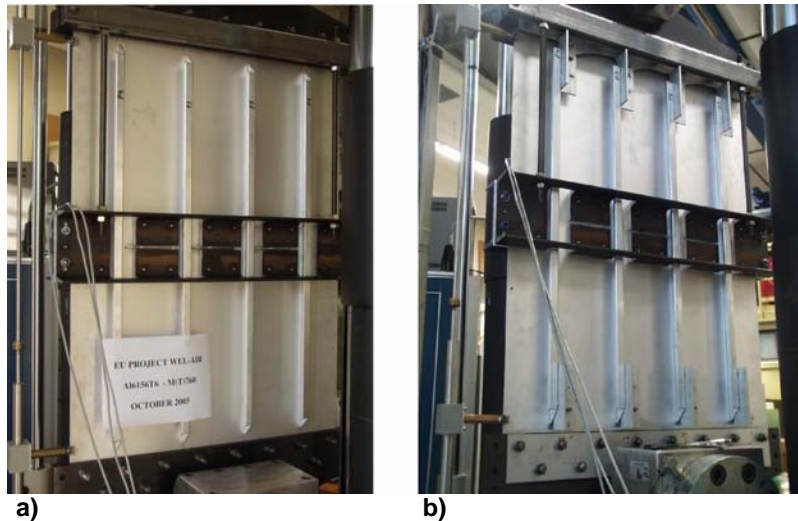
Figure 4: Testing of the panels; a) with four stringers and three clips and b) four stringers without clips.

One type of the four-stringer panels (**Figure 2a**) additionally contained welded clips perpendicular to the stringers to represent a possible structural situation for future airframe applications. The initial notch length ($2a_0 =$ 115 mm) of these panels was slightly shorter than the length of the welded clip (120 mm) and yielded crack to width ratio of $a_0/W=0.15$ to investigate the residual strength of such panels. The aim of the investigation of these 760 mm wide and 1200 mm long four-stringer panels were to examine the effect of clip weld joint on the panel performance and use for the application of the FITNET FFS Procedure. Furthermore, observation of interaction between stiffener and clip welds, which is necessary for better description of such newly designed panels, was also intended. The notches were located along the weld length of the clips and placed at the weld toe, as shown in **Figure 3a**. The four-stringer panels were loaded from skin and stringer ends (**Figure 4**) while two and three-stringer panels were loaded from skin parts only due to different test and analysis requirements. Specially made rigid anti-buckling guides (made of heavy steel parts) were used in all of the tests to maintain the mode I type of loading during the deformation and fracture process, as shown in **Figure 4**.

All panels were tested under quasi-static loading using a 1000 kN servo-hydraulic test machine at room temperature under displacement control with a loading speed of 0.2mm/min. During the tests, the crack tip opening displacements (CTOD) using special clip gauges (so-called δ_5) at the both crack tips, crack mouth opening displacement (CMOD) and total panel elongation (V_{LL}) were measured and recorded in addition to the load.

FITNET FFS ANALYSIS PRINCIPLES

FITNET FFS Procedure uses both FAD and CDF routes. The Crack Driving Force (CDF) approach of the residual strength analysis using the fracture module of the FITNET FFS procedure is shown schematically in **Figure 5**. The CDF curve is plotted in a diagram together with the respective R-curve of the material of interest. Throughout the entire analysis, the definition of CTOD δ_5 is used for the crack tip opening displacement. The type of the fracture parameter that will be used in CDF diagram is specified by the available R-curve, i.e. if for instance, the R-curve is measured in terms of CTOD δ_5, then CDF should also be determined using CTOD δ_5 as the crack tip parameter. The tangency condition between the R-curve and CDF curve gives the maximum load carrying capacity of the component under consideration. At each intersection point between the R-

curve and CDF curve (points "1" to "4" in **Figure 5**), the respective load together with the stable crack extension and the corresponding crack tip loading are found. By plotting the load versus stable crack extension (Δa) or load versus crack tip loading, a structural response can be predicted. In this way, the validation of the analysis is not only based on the comparison of the predicted and experimental maximum load values, but also in terms of the load-deformation behaviour of the specimen or component.
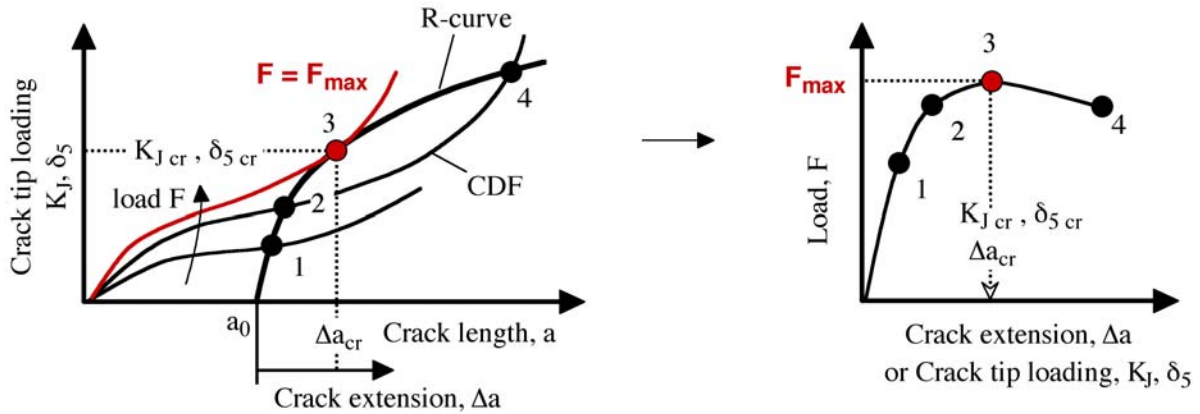

Figure 5: Principle of the residual strength analysis in the FITNET FFS procedure using the CDF approach.

INPUT INFORMATION FOR FITNET ANALYSIS

Material related input: Tensile properties and fracture resistance

The CTOD(δ_5) R-curves were determined using multiple specimens (minimum of eight specimen for each R-curve) method. The stable crack extensions (Δa) were measured optically from the fracture surfaces after the tests. The generated material related input information is summarized in **Table 1**. Additionally, all weld metal tensile properties were also determined by testing of specially made 0.5 mm thick and 2.0 mm wide "micro-flat-tensile" (MFT specimens to establish the strength mis-match levels. However, these values are not listed in **Table 1**, since the panel and crack configurations used in this study did not require this information. The R-curves are given in terms of fitted curves. Both for skin and stringer materials of Al6013-T6 which is the material of two and three stringer panels, the R-curves are the same as shown in **Table 1**. However, for the Al6156-T6 material of four stringer panels, two different R-curves were determined. In addition to standard specimens for base materials, numbers of C(T)50 specimens were extracted from T-joints (preserving original T-joint configuration) by placing the notch tip to the weld toe to determine the R-curves of the welded joints. These specimens are named as "structure-like" specimens to generate fracture toughness data under somehow similar constraint conditions as the cracked component (here flat panels). The crack was located along the weld seam and hence, it was intended to generate R-curve of the Al6156-T6 material in a realistic manner.

a)   b)
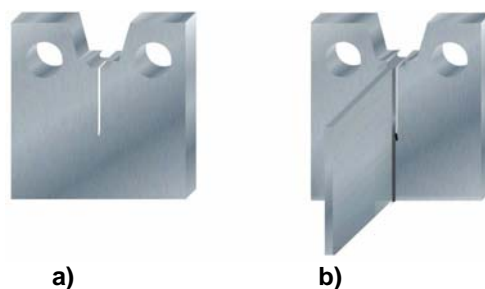
Figure 6: The specimens used for fracture toughness measurements; a) BM standard C(T)50 specimen and b) C(T)50 specimen preserving T-joint geometry of original structure.

Component related input: K-factor and yield load solutions

For the analysis of central crack in the skin between two stringers (i.e. one-bay crack, **Fig. 2d**), crack growth both in the skin and stringer (so-called "crack branching") should be considered. In order to cover such complex fracture behaviour with two crack tips, a special approach should be adopted to determine the K-solutions for each crack tip. Therefore, the K-factor solutions for both the skin and stringer crack, which occurs after crack branching, have been derived by the finite element analysis using the crack closure integral method [13]. The details of this analysis are given in Ref. [11]. However, existing K-solution (as given in Annex A of the FITNET FFS Procedure, Vol. III) for the M(T) panels can be used as a "simplified approach" for the FITNET FFS analysis. Hence, the following solution, eqn. (1) of a M(T) panel was used:

$$K = \frac{F}{2WB} \sqrt{\pi a} \sqrt{1/\cos\left(\frac{\pi a}{2W}\right)}$$

(1)

where F is the externally applied load, 2W and B are the panel width and thickness, respectively, and a is the half crack length.

The yield load solution for the two and three stringer panels was simply estimated from the unstiffened M(T) panel solution, as given in **Eq. (2)**:

$$F_Y(a) = R_{p0.2} 2WB(1 - a/W)$$

(2)

where $R_{p0.2}$ is the yield strength of the material.

It is important to describe the definitions of the failure criteria of the stiffened thin-walled panels and accordingly conducting the residual stress analysis.

Table 1: Material properties of the Al6013 T6 and Al6156 T6

Material	Tensile properties	Fracture resistance
Al6013-T6 (For two- and three-stringer panels)		
Skin material	E = 69000 MPa $R_{p0.2\ sk}$ = 385 MPa $R_{m\ sk}$ = 400 MPa	$\delta_5 = 0.30\Delta a^{0.63}$
Stringer material	E = 69000 MPa $R_{p0.2\ str}$ = 405 MPa $R_{m\ str}$ = 425 MPa	$\delta_5 = 0.30\Delta a^{0.63}$
Al6156-T6 (For four-stringer panels; with and without welded clips)		
Skin and Stringer	E = 69000 MPa $R_{p0.2\ str}$ = 346 MPa $R_{m\ str}$ = 399 MPa	$\delta_5 = 0.245\Delta a^{0.726}$ Skin and Str. $\delta_5 = 0.217\Delta a^{0.662}$ LBW T-joint

The types of failure criteria can be described as follows;
- Skin and stringer failure
- Skin failure

Skin and stringer failure criteria

The residual strength of a riveted stiffened structure (differential design) of a conventional airframe is defined by the failure load of the cracked skin as well as of the stiffening element. The rivets also provide a potential source for the

damage initiation and failure occurrence in such stiffened structures. The skin failure occurs if the crack tip stress intensity exceeds a critical value, whereas the stiffening element fails if the stress level exceeds the tensile strength of the stiffener material. Due to the nature of the new welded stiffened structures (integral design), the skin crack may easily propagate also into the stiffener. This event creates the fundamental difference between "differential design" where skin and stringer are still two separate components (mechanically fastened) and "integral design" where skin and stringers are metallurgically joined. Therefore, the stiffener failure may also be caused by an unstable growth of the stiffener crack in "integral design" geometries, such as welded panels. Contrary to the riveted structure, the prediction of the stiffener failure load in these panels with welded stringers requires a fracture mechanics based analysis.

Skin failure criterion

The skin and stringer analyses are separate and analysis routes do not interact. However, they are coupled through the K-factor solutions. It is obvious that the stress intensity at the skin crack tip can be significantly affected by the amount of the stringer damage, i.e. the length of the stringer crack. While determining the K-factor in the numerical analysis, the assumption (based on the experimental evidence) of equal lengths of the stringer crack and the part of the skin crack beyond the stringer (**Figure 7**) has been made leading to a K-factor solution which is unique for this particular skin and stringer crack constellation (see for example **Figure 2d**). For the application of

the FITNET FFS procedure to the skin crack problem, the yield load, F_Y, and K-factor solutions are needed in order to calculate the crack tip loading. The crack tip loading of the skin crack is given by:

$$\delta_5 = \delta_{5e} \times \left[f(L_r) \right]^{-2} \tag{3}$$

with

$$\delta_{5e} = \frac{K_{Isk}^2}{R_{p0.2}E} \tag{4}$$

where $R_{p0.2}$ is the yield strength and E is the Young's modulus of the base material. KIsk is the stress intensity factor of the skin crack. Moreover,

$$L_r = \frac{F}{F_Y(a)} \tag{5}$$

where F is the applied load and $F_Y(a)$ is the yield load as a function of the skin crack length, a.

The residual strength of the stiffened panel based on the skin failure criterion is defined by the unstable growth of the skin crack (tangency condition between the crack driving force and the R-curve):

$$\delta_5 = \delta_{5R} \quad and \quad \frac{\partial \delta_5}{\partial a} = \frac{\partial \delta_{5R}}{\partial a} \tag{6}$$

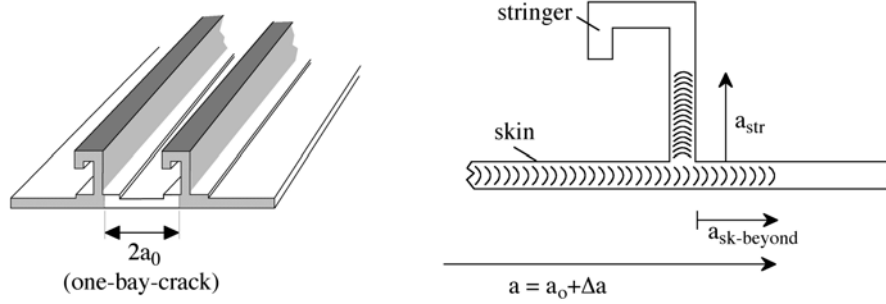where δ_{5R} is the R-curve of the skin material.

The plastic collapse condition is met, if the applied load provokes a net section stress reaching the collapse stress of the skin material:

$$L_r \geq L_{r\,max} \tag{7}$$

with

$$L_{r\,max} = \frac{1}{2} \frac{R_{p0.2} + R_m}{R_{p0.2}} \tag{8}$$

where R_m is the tensile strength of the skin material.

Figure 7: Crack branching (into skin and stringer) in an integrally (welded) stiffened panel when growing crack approaches to the stringer in a perpendicular form.

Stringer failure criterion

The analysis of the stringer failure criterion is similar to that of the skin analysis. Here, the stringer is treated as a separate structural element containing a crack. The application of the FITNET FFS procedure to the cracked stringer is in principle identical to that of the skin problem. The only difference is that the stress intensity factor and yield load of the stringer instead of the skin should be used in respective equations presented above. The stress intensity factor K_{Istr} for the stringer crack should be used in
Eq. (4). The normalized load is

$$L_{rstr} = \frac{F_{str}}{F_{Ystr}(a)}$$ (9)

where F_{str} is the load acting in the stringer due to external load F applied to the stiffened panel and

$$F_{Ystr}(a) = R_{p\,0.2str}(A_{str} - a_{str}B_{str})$$ (10)

is the stringer yield load, F_{Ystr}, defined in terms of the stringer net section yielding. $R_{p0.2str}$ is the yield strength of the stringer material, A_{str} is the total cross-sectional area of a stringer, a_{str} is the stringer crack length (see **Figure 7**), and B_{str} is the thickness of the stringer foot.

Due to the assumption $a_{str}=a_{sk-beyond}$ (**Figure 7**), the stringer crack length, $a_{str,}$ is linked to the skin crack length, a. Thus, the stringer yield load results in a function of the skin crack length, a. In order to find the structural response of the stiffened panel based on the stringer failure criterion, the link between the stringer load, F_{str}, and the total applied load, F, to the stiffened panel is needed. This link is given through the stringer load concentration factor $L_s(a/W)$ which defines the ratio of the stringer load of a cracked stiffened panel to the stringer load of an uncracked stiffened panel. Since the stringer carries more load in a cracked panel due to the load transfer from the damaged skin to the stringer, the stringer load concentration factor is greater than 1 ($L_s(a/W) > 1$). The externally applied load, F, to the stiffened panel is then determined as follows [11]:

$$F = \frac{F_{str}}{\chi L_s(a/W)} \quad with$$ (11)

$$\chi = \begin{cases} 0.095 \\ 0.061 \end{cases} \quad for \quad \begin{matrix} 2 - stringer \\ 3 - stringer \end{matrix}$$

**RESULTS AND DISCUSSION**

The two- and three-stringer panels

Figures 8a and **8b** show the FITNET predictions in terms of load-CTOD δ_5 curves using both the skin and stringer failure criteria. In the analysis based on the skin failure criterion, the yield load solution is taken as that of an M(T)

panel, i.e. the skin is assumed to yield at the same load level as a simple M(T) panel (without stringers) of the same width and thickness. However, the K-factor solution here incorporates the stringer effect and was derived from FEA [11]. In the analysis based on the stringer failure criterion, the stringer yield load is simply based on the stringer net section yielding. The K-factor solution for the stringer crack was also derived from FEA as for the skin crack. The result of this analysis shows that the residual strength of the stiffened panel is governed by the skin failure since the analysis based on the skin failure criterion predicts a lower maximum load than that based on the stringer failure criterion. At the load level which results in unstable fracture of skin crack, the stringer crack remains stable. The difference between analysis Option 1, in which only the yield and ultimate tensile strength are used as
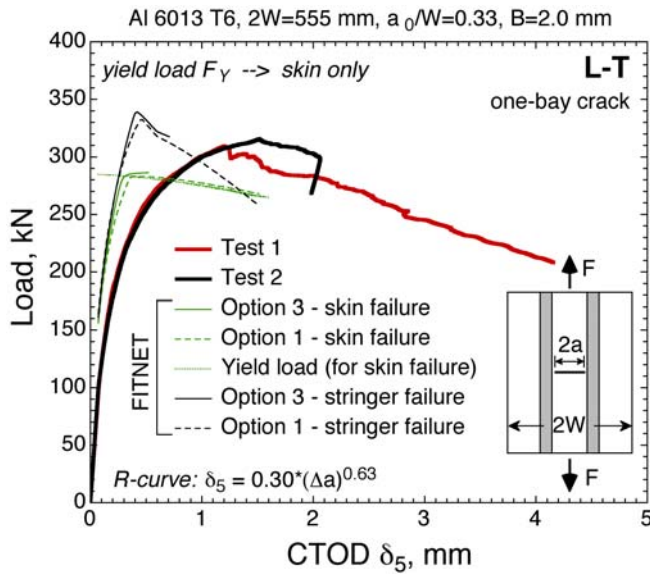

Figure 8a: Comparison between experiments and FITNET FFS prediction of a residual strength for 2-stringer panel with one-bay crack.
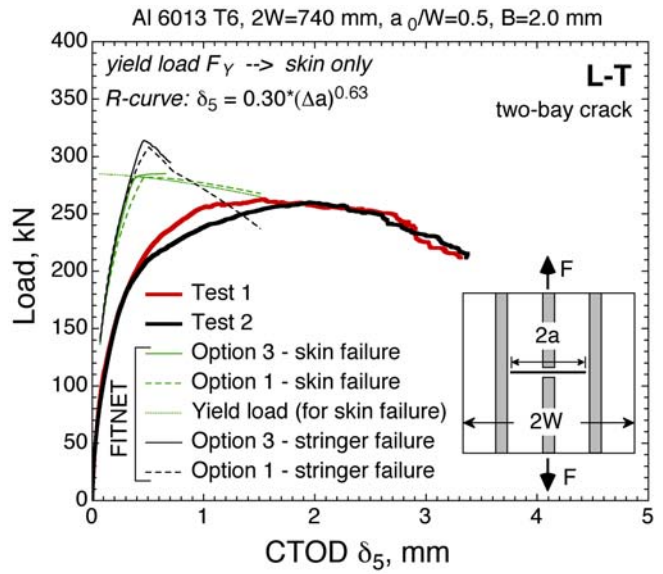
Figure 8b: Comparison between experiments and FITNET FFS prediction of a residual strength for 3-stringer panel with two-bay crack.
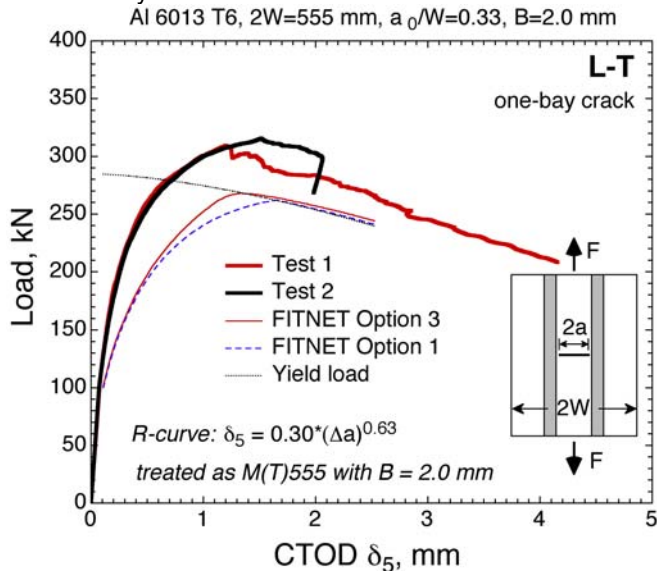

Figure 9a: FITNET FFS prediction of a residual strength for 2-stringer panel with one-bay crack using the "*simplified approach*" by treating the stiffened panel as a simple M(T)555 panel.
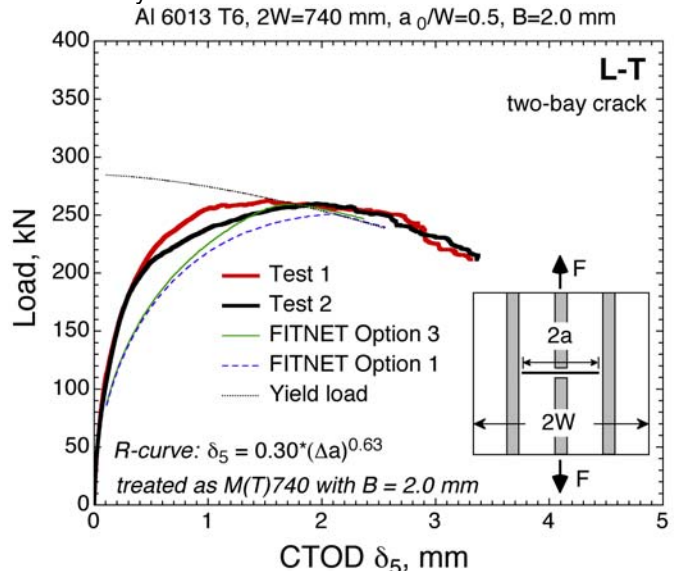

Figure 9b: FITNET prediction of a residual strength for 3-stringer panel with two-bay crack using the "*simplified approach*" by treating the stiffened panel as a simple M(T)740 panel.

material parameters, and Option 3 requiring the full stress-strain curve is small. This is attributed to the almost perfectly plastic behaviour of the material analyzed. The conservative estimate of the strain hardening exponent by the yield and ultimate tensile strengths in Option 1 fairly accurately describes the real stress-strain curve as used in Option 3. Using a simplified approach in which the stiffened panels are treated as simple M(T) panels with ignored stiffener effect. That is, the yield load and the K-factor solutions of equivalent M(T) (skin) panels were used. The FITNET FFS procedure yields conservative predictions, as shown in **Figures 9a** and **9b** for 2- and 3-stringer panels, respectively. This analysis method enables the prediction of the maximum load carrying capacity of stiffened panels as a whole but without assigning the failure to single structural elements like stringer and skin.

When comparing the predicted with the experimental load-CTOD δ_5 curves in **Figures 8a** and **b**, it is clearly visible that at a given applied load the predicted CTOD δ_5 value is underestimated. This observation suggests that the K-factor has been underestimated in the FEA, which is attributed to the boundary conditions of the anti-buckling guides. These have been realized by suppressing the out-of-skin-plane displacements. However, in the experiment the stiffened panels may have experienced slight out-of-plane deformation which has a strong effect on the K-factor solution as a sensitivity study with FEA has shown (**Figures 10 and 11**).
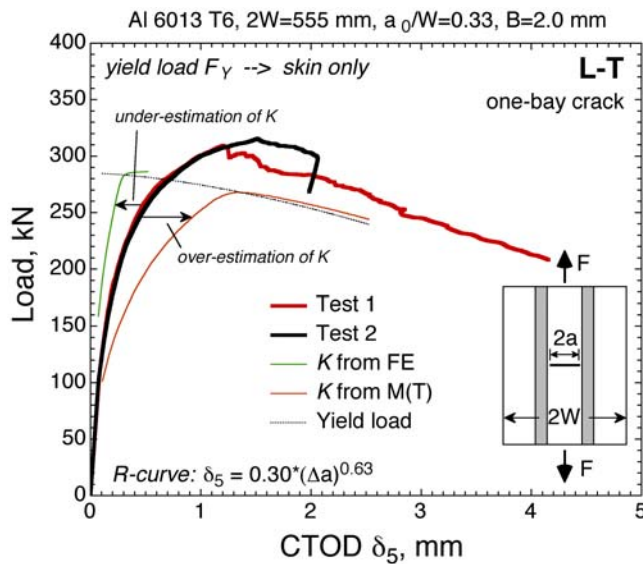
Figure 10: Sensitivity analysis of the FITNET FFS predictions to the K-factor solution in a two-stringer panel with one-bay crack.
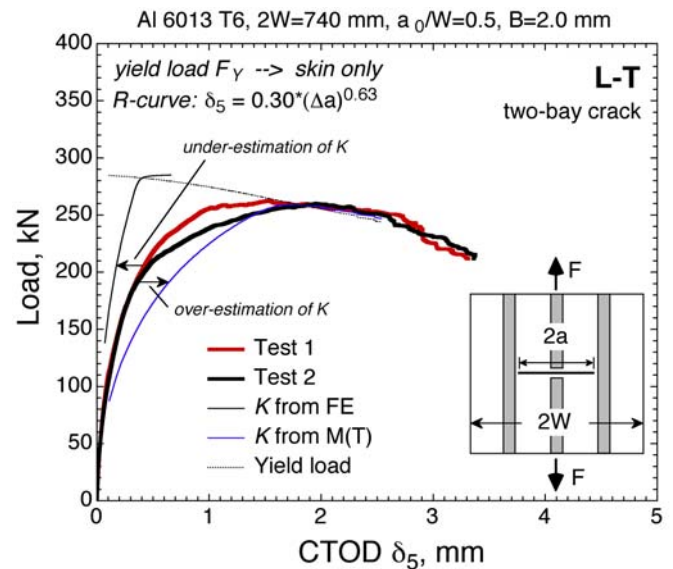
Figure 11: Sensitivity analysis of the FITNET FFS predictions to the K-factor solution in a three-stringer panel with two-bay crack (central stiffener is broken).

Four-stringer panels with and without clips

The 4-stringer panels were analyzed using the simplified approach as presented above. The **Figure 12** shows the comparison between experimental and FITNET FFS Option 1 and Option 3 analysis results. As it can be seen, the predicted load carrying capacity of the panel is very close to the test result with a small amount of non-conservatism. For both analysis, the R-curve of the base metal obtained from testing of C(T)50 specimens. The original skin thickness was 2.52 mm, however, equivalent skin thickness of 3.17 mm was used by including the stringers into the base panel to conduct so-called "simplified" analysis.

Option 3 of the analysis has slightly increased the non-conservatism compared to the Option 1 result. The Option 1 analysis has yielded a prediction which was already non-conservative. The reason for these non-conservative predictions is in the use of R-curve which was obtained from another batch of material than those of wide plates tested. Additionally, the use of simplified approaches may have lead to these non-conservative results. However, the fracture module of the FITNET FFS Procedure is generally able to provide good predictions for such a complex configuration. Similarly, analysis was also conducted for the four stringer panels with three welded clips (see

Figure 2a) and **Figure 13** shows the comparison between experimental result and FITNET FFS predictions. The
maximum load attained during the test was predicted rather accurately. In this analysis, the effect of the selection
of the fracture toughness (R-curve) input information on the prediction was studied. It is of importance to establish
the sensitivity of the Option 1 predictions on the selection of the R-curves. The use of the R-curve of the T-joints
(generated from testing of non-standard but structure-like C(T) 50 specimens, see **Figure 13**) provides best
prediction with slight conservatism (see blue curve in **Figure 13**). These structure-like specimens may provide
somehow similar microstructural (and to certain extend constraint) conditions as component itself. The use of
Option 3 route with the use of the same R-curve data has again increased the predicted maximum load level.
Hence, it is clear that the usage of fracture toughness data obtained from testing of the "structure-like" specimen
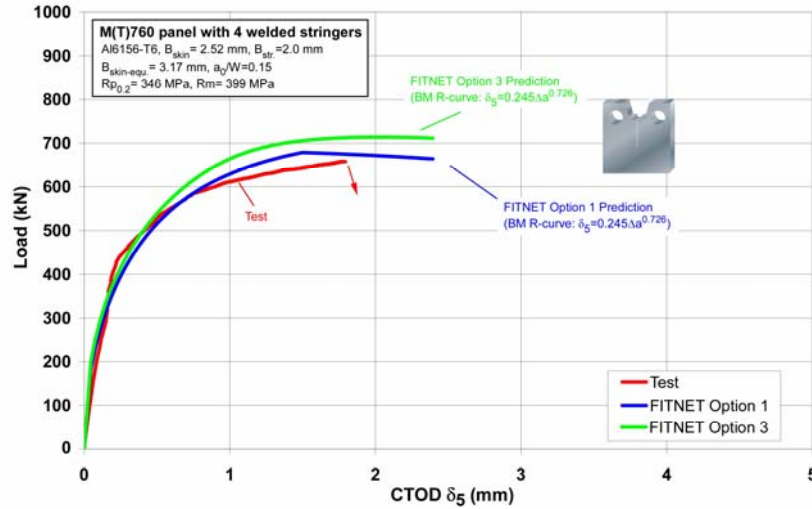data provided good residual strength predictions.

Figure 12: Comparison between experimental curve and predicted load vs. CTOD δ_5 curves of the four stringer
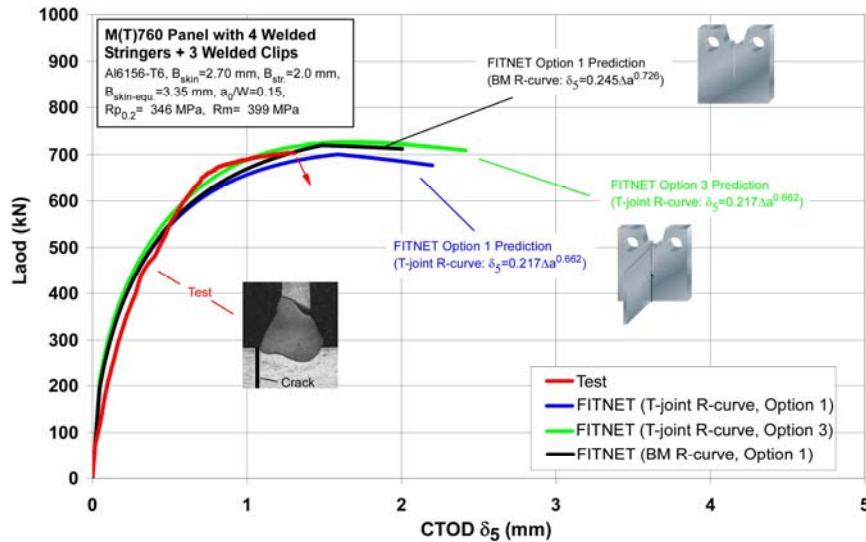panel. The FITNET FFS Procedure was applied for both Option 1 and Option 3.

Figure 13: Comparison between experimental and FITNET FFS predictions of the Load vs. CTOD δ_5 curves
of the four stringer panel with three welded clips.

CONCLUSIONS

This investigation has demonstrated the ability of the fracture module of the newly developed FITNET FFS Procedure to predict the residual strength of the complex thin-walled welded structural components. The method applied to the laser beam welded two- and three-stringer panels incorporating both the skin and stiffener failure criteria enable a prediction whether the skin or the stringer will fail under the given loading condition. In order to apply the proposed fracture mechanics approach to the cracked stringer after crack branching in an integrally stiffened welded panel, the K-factor solution for the stringer crack is essential. However, this is generally not available in current handbooks and also in the FITNET FFS Annex A due to the novelty of welded structures in the aerospace industry and, therefore, needs to be the generated by an additional analysis.

The "simplified approach" by treating the two- and three-stringer panels as a simple M(T) panel has shown a potential to predict the residual strength of such complex stiffened panels with a reasonable accuracy. FITNET FFS analyses of the four-stringer panels have provided very good estimates of the load carrying capacity justifying the approximation of welded panels with stringers to simple base metal M(T) specimens with appropriate equivalent thickness. It was also found that the usage of the R-curves obtained from "structure-like" specimens in the FITNET FFS gives more accurate estimates.

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