Development of Custom 465[®] Corrosion-Resisting Steel for Landing Gear Applications

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Existing high-strength low-alloy steels have been in place on landing gear for many years owing to their superior strength and cost performance. However, there have been major advances in improving the strength of high-performance corrosion-resisting steels. These materials have superior environmental robustness and remove the need for harmful protective coatings such as chromates and cadmium now on the list for removal under REACH legislation. A UK government-funded collaborative project is underway targeting a refined specification Custom 465[®] precipitation hardened stainless steel to replace the current material on Airbus A320 family aircraft main landing gear, a main fitting component developed by Messier-Bugatti-Dowty. This is a collaborative project between Airbus, Messier-Bugatti-Dowty, and Carpenter Technology Corporation. An extensive series of coupon tests on four production Heats of the material have been conducted, to obtain a full range of mechanical, fatigue, and corrosion properties. Custom 465[®] is an excellent replacement to the current material, with comparable tensile strength and fracture toughness, better ductility, and very good general corrosion and stress corrosion cracking resistance. Fatigue performance is the only significant area of deficit with respect to incumbent materials, fatigue initiation being often related to carbo-titanium-nitride particles and cleavage zones.

Keywords aerospace, corrosion, failure analysis, landing gear, mechanical, stainless steel, wear

1. Introduction

For 50 years the material selection for the main structural components of landing gear has been ultra-high tensile strength (UHTS) steels such as AISI 4340 and derivatives including 35NCD16THQ and 300M. These low-cost materials provide excellent tensile strength, fatigue resistance, and good fracture toughness (Ref 1, 2). The key disadvantage is poor corrosion resistance of the untreated metal; thus the metal surface is coated to provide protection. A number of coatings are applied in the manufacturing process including cadmium (typically chromated, primed, and painted) and chrome. The cadmium coating is for corrosion resistance on external surfaces and chrome is for wear resistance on internal surfaces where the sliding member (or piston) strokes within the pressurized barrel section of the main fitting (or outer cylinder). The cadmium coating provides excellent protection to the underlying steel in the in-service environment. Cadmium has a very slow corrosion

List of Symbols

A_1 to A_4	Parameters in the equivalent strain fatigue model
C	Lower tolerance limit confidence level
C_1	Confidence level about the regression line
C_2	Confidence level on the standard deviation of the least-squares fit
C^*	Value that satisfies the double integral expression
E	Elastic modulus (MPa)
$\exp$	Natural exponential function
g, h, ρ	Generic functions
K'	Cyclic strength coefficient (MPa)
K_{1C}	Fracture toughness mode I (MPa $\sqrt{m}$)
K_{ISCC}	Stress corrosion cracking mode I (MPa $\sqrt{m}$)
K_t	Stress-concentration factor
$\log$	Logarithm to base 10
N	Number of cycles to failure
n'	Cyclic strain hardening exponent
n	Sample number
P	Proportion of the normal distribution
R	Surface roughness (μm)
R_a	Arithmetic mean of absolute values of surface roughness (μm)
R_e	Strain ratio
SD	An estimate of the standard deviation log (N); equivalent to the estimate of the standard deviation of the fit
s	Sample standard deviation
s_{eq}	Standard deviation of the equivalent true strain
u, v	Generic variables
z_P	Standard normal variant for the proportion P of a normal distribution
α	Scale parameter of the Weibull distribution
β	Shape parameter of the Weibull distribution
β_3, β_{offset}	Constants in the determination of the elastic mean stress response

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ε_a	True strain amplitude
$\Delta\varepsilon$	Total true strain range
$\Delta\varepsilon_e$	Total true elastic strain range
$\Delta\varepsilon_p$	Total true plastic strain range
ε_{eq}	Equivalent true strain
$\varepsilon_{eq,a}$	Upper limit of equivalent true strain
$\varepsilon_{eq,b}$	Lower limit of equivalent true strain
$\bar{\varepsilon}_{eq}$	Average equivalent true strain
μ	Sample mean
ξ_a, ξ_b, ξ_{ab}	Generic functions
σ_a	True stress amplitude (MPa)
$\Delta\sigma$	The total true stress range (MPa)
σ_{max}	Maximum true stress (MPa)
σ_m	Mean true stress (MPa)
τ	Threshold parameter of the Weibull distribution
χ^2	Chi-squared distribution

rate and provides limited sacrificial protection if the coating is compromised, helping to protect the exposed steel (Ref 3).

Registration, Evaluation, Authorization and restriction of CHemicals (REACH) has listed cadmium and chromates used in paints and the manufacture of chrome coating as substances of very high concern (SVHC). Chromates are on the authorization list with a sunset date of September 2017 (Ref 4). Cadmium is on the candidate list and is expected to be placed on the authorization list in the near future. The imminent restriction in the use of cadmium and chrome-coated UHTS steels in landing gear has driven research into alternative material selection for the main fitting and sliding member.

The recent development of high-performance structural stainless steels, that have tensile properties approaching the current UHTS steels, offers a promising alternative material for landing gear. These precipitation-hardened (PH) corrosion-resisting steels (CRES) remove the need for a sacrificial metallic coating to protect external surfaces. Interfaces may require a sacrificial coating where galvanic corrosion is a concern; an alternative coating to replace cadmium for this application is zinc-nickel (Zn-Ni). Although this coating is currently REACH compliant, its use with the current UHTS does not solve corrosion problems if the coating fails. An alternative wear-resistant coating recently certified on in-service aircraft is high-velocity oxygen fuel (HVOF)-applied tungsten carbide-cobalt-chrome (WC-Co-Cr) coating. Ultimately, the use

of stainless steels provides a more robust solution, reducing maintenance time and costs caused by corrosion and facilitating an increase in the time between overhaul.

A competitor to high-performance stainless steel is new high-performance titanium alloy. However, the use of titanium alloys is limited by the much higher cost of the material compared with CRES alloys and the current UHTS steels. Additionally, the use of titanium necessitates a volume increase in a component which is not possible in the A320 family aircraft where space is at a premium.

A current research programme termed CREST is investigating the replacement of 35NCD16THQ currently used on A320 family aircraft main landing gear main fitting with Carpenter Custom 465[®] stainless steel (C465). CREST is a collaborative project between Airbus, Messier-Bugatti-Dowty (MBD), and Carpenter Technology Corporation (CRS). C465 is a proven aerospace material currently used on the A350 bogey pivot pin.

The CREST programme has included an extensive campaign of material property testing on four production Heats of C465 with a refined specification. This has generated confidence in the material properties of C465. Each production Heat was drawn-down to give a range of reduction ratios found on the main fitting. This paper reports the results of the testing campaigns on the mechanical properties of C465. The results have been analyzed to calculate material allowables for the various mechanical properties.

2. Experimental Procedures

2.1 Material Production

Four production Lots/Heats of C465 (762 mm (30") diameter) were produced, using the vacuum induction melting-vacuum arc remelting (VIM-VAR) process. The four ingots were press forged to 317.5 mm (12.5"), 266.7 mm (10.5"), 215.9 mm (8.5"), and 165.1 mm (6.5") diameter billets labeled H1 to H4, respectively. These diameters represent the range of reduction ratios from the 30" ingot typically found in a main fitting forging. The composition of each billet is presented in Table 1. Each billet meets the AMS5936C chemistry standard (Ref 5). A further VIM-VAR-manufactured hot-rolled 88.9 mm

Table 1 Composition in wt% of the C465 billets (balance Fe not shown)

Element	Composition in wt%					
	AMS5936C limits (Ref 5)		H1: 12.5 in (317 mm)	H2: 10.5 in (267 mm)	H3: 8.5 in (216 mm)	H4: 6.5 in (165 mm)
	Minimum	Maximum				
C	...	0.02	0.0051	0.0041	0.0037	0.0023
Mn	...	0.25	0.01	0.01	0.01	0.01
Si	...	0.25	<0.01	0.02	<0.01	0.01
P	...	0.015	0.002	0.003	0.002	0.002
S	...	0.010	<0.0005	0.0011	<0.0005	0.0009
Cr	11.00	12.50	11.51	11.63	11.58	11.52
Ni	10.75	11.25	11.00	11.01	11.00	11.06
Mo	0.75	1.25	0.95	0.93	0.94	0.95
Ti	1.50	1.80	1.59	1.62	1.61	1.63
N	...	0.01	<0.0014	0.0012	<0.0011	0.0015

(3.5") diameter rod was produced with nominal chemistry (Table 1) to investigate stress corrosion cracking.

2.2 Coupon Preparation

Blanks were extracted from mid-radius locations away from the billet ends, with representative melting/remelting conditions. The blanks were then machined into oversized coupons for solution heat treatment. The solution heat treatment was 1 h

Fig. 1 Optical micrograph of C465 etched with Ralph's reagent at 500 $\times$ magnification

at 982 °C (1800 °F) followed by an oil quench. Within 24 h post-quench, the coupons were refrigerated for 8 h at -73 °C (-100 °F) and then air warmed.

Following solution heat treatment, fatigue, salt spray corrosion, and stress corrosion cracking test coupons were immediately aged. Coupons for the remaining tests were machined to size and then single point turned/machined to finish before aging. The aging treatment was for 4 h at 510 °C (950 °F) and then air cooled to ambient temperature. The test coupons were then in the H950 condition. Figure 1 is a micrograph showing the typical martensitic microstructure of C465 in the H950 condition, etched using Ralph's reagent (Ref 6).

Post aging, fatigue coupons were low-stress ground to size, stress relieved as per AMS 2759/3E section 3.4.4 (Ref 7), and finish machined. The surface finish on the fatigue specimens was $R_a \leq 0.16$ μm , maximum $R \leq 1.6$ μm . Salt spray corrosion and stress corrosion cracking test coupons were machined and ground to finish after aging. The notch was milled in the fully heat-treated condition. None of the coupons were shot-peened.

2.3 Test Procedures

All tests were managed by the University of Sheffield Advanced Manufacturing Research Centre (AMRC), except stress corrosion cracking tests conducted internally at CRS. Table 2 describes the tests conducted on the four Heats plus the 3.5" bar of C465, the standards used, and the test piece drawing. Attention was paid to the grain orientation of the

Table 2 Coupon tests carried out on the five Heats of C465

Test	ASTM/ISO standard	Test piece drawing	Grain orientation
Tensile/ductility	E8/E8M-9 (Ref 8)	Length: 130 mm	Longitudinal & Transverse
Tensile yield (0.2%) (TYS)		Test length: 88 mm	(Long. & Trans.)
Ultimate strength (UTS)		Gage length: 62 mm	
% Elongation (5D basis) (%EL)		Gage length diameter: 11.27 mm	
% Reduction in area (%RA)		Radius of fillet: 12.7 mm	
		Permitted taper: 0.03 mm	
		Coaxiality of thread: 0.03 mm	
Compressive yield strength (CYS)	E9-09 (Ref 9)	Per Table 2 in ASTM E9	Long.
Shear strength (SS)	B769-07 (Ref 10)	Length: 80 mm	Long.
		Diameter: 6 mm	
Fracture toughness (K_{IC})	E399-09 (Ref 11)	Per fig. A4.1 in ASTM E399	L-C & C-L (a)
			L: longitudinal (along billet)
			C: circumferential
Salt spray corrosion	B117-09 (Ref 14)	From material offcuts:	Trans.
		645 mm ² (1 in ²) exposed area	
Stress corrosion cracking (K_{ISCC})	F1624-05 (Ref 13)	Length: 57 mm	L-C(a)
		Width/depth: 10.2 mm	
		Notch depth: 3.6 mm	
		Angle: 45°	
		Notch tip curvature: 0.13 mm	
Micrographic Examination (grain size)	E112/10 (Ref 15)	From material offcuts	Coupons extracted from
			inner, middle, and
			outer radius of billet.
Strain-controlled fatigue	E606/E606M-12 (Ref 12)	Length: 105 mm	Long. & Trans.
		Test length: 54.5 mm	
		Gage length: 16 mm	
		Gage length Diameter: 7.98 mm	
		Radius of fillet: 11/57 mm	

(a) 2 letter code: first letter denotes the direction normal to the crack plane and second letter the expected direction of crack propagation

coupons. All testing was conducted at room temperature except the salt spray corrosion test (conducted at 35 °C (95 °F)).

Tensile, compressive, and shear strength tests and fracture toughness tests were carried out using ASTM procedures (Table 2, Ref 8-11). Tensile, compression, and shear tests were conducted on at least twenty, three, and two coupons per Heat, respectively. Fracture toughness tests were conducted on five or six coupons per Heat.

Fatigue tests were carried out under strain control using an internal MBD test procedure based on Ref 12. Tests were conducted under strain control up to 85,000 cycles at 0.25-1 Hz. The sample was then set up under load control at a maximum of 10 Hz up to 10^6 cycles when the test was stopped. Load control was based on the cyclic minimum and maximum stress after 85,000 cycles. Three strain ratios ($R\epsilon$) were tested with $R\epsilon = -1.0$, 0.1, and -2.0 all with a stress intensity factor, $Kt = 1.0$. For $R\epsilon = -1.0$, fifteen longitudinal coupons from each Heat were tested at a maximum strain to achieve target lives of 100, 1000, 10,000, 50,000, and 200,000 (three duplicates per target life). Additional transverse coupons were tested to give target lives of 1000 and 200,000. For $R\epsilon = 0.1$ and -2.0 , longitudinal coupons from H1 and H4 were tested at a maximum strain to achieve target lives of 1000, 10,000, and 50,000 (three duplicates per target life). Additional transverse coupons were tested at $R\epsilon = 0.1$ from H2 and H3 to give target lives of 1000 and 200,000. All fatigue specimens that suffered failure were examined under a scanning electron microscope (SEM) with an attachment to perform energy-dispersive x-ray spectroscopy (EDX) to determine the failure mode and the point of initiation.

Stress corrosion cracking tests on the 3.5" bar were conducted using the rising step load (RSL) procedure (Ref 13). The coupons were cleaned but not passivated. The open circuit potential of C465 submerged in natural 3.5% NaCl solution was measured to be -0.350 V versus the Saturated Calomel Electrode (SCE) after 48 h. Tests were conducted with the coupons potentiostatically held at -0.350 V (SCE).

Salt spray corrosion tests (Ref 14) were conducted on coupons with an exposed 25.4 mm (1") square surface with transverse grain orientation. The exposed surface was cleaned but not passivated. The remaining surfaces were coated with an acetate lacquer. The samples were loaded into a cabinet with the exposed surface at a 20°-25° angle from the vertical. Eight samples per Heat were tested with two removed after 24 h, two removed after 500 h, and the remaining four left for the duration of the experiment (1000 h) or until red rust was observed. Note for H3 and H4, only two samples were tested to 1000 h. Analysis of the test coupons was restricted to visual inspection of the surface only.

Grain size tests were conducted on 12.7 mm (1/2") cube coupons, cut to minimize damage (heating and/or cold work) using a wet abrasive saw, ready to be polished and etched as per ASTM standard E112 (Ref 15).

3. Results

3.1 Tensile Strength

Figure 2 presents the distribution of 184 tensile yield strength (TYS) observations separated into the four Heats and the two grain orientations. Each bin is over a range of 4 MPa, and each point is centered at the median of the bin.

Fig. 2 Distribution of TYS data from the four Heats of C465: (a) longitudinal and (b) transverse

Fig. 3 Distribution of UTS data from the four Heats of C465: (a) longitudinal and (b) transverse

The transverse and longitudinal tensile results show that H1, H2, and H4 follow a normal probability distribution. The transverse TYS data from H1 do have a larger variance compared with H2 and H4. H3 shows a bimodal distribution. In the longitudinal orientation, the split on H3 appears to be values ≥ 1730 MPa ($n = 10$) and values ≤ 1730 MPa ($n = 14$). Likewise in the transverse orientation, the split on H3 appears to be values ≥ 1727 MPa ($n = 14$) and values ≤ 1727 MPa ($n = 10$). The mean TYS for H3 is similar to H2 and H4 in each orientation but the variance is greater. H1 has a lower mean TYS than the other three Heats; however, the difference is small at 22 and 21 MPa in the longitudinal and transverse orientations respectively.

Figure 3 shows the distribution of the 184 ultimate tensile strength (UTS) observations in the similar manner as the TYS observations. Each bin is over a range of 4 MPa. The UTS for each dataset shows the same form of distribution as seen for the TYS. The bimodal distribution in H3 is split at values ≥ 1816 MPa ($n = 10$) and values ≤ 1816 MPa ($n = 14$) in the longitudinal orientation, and at values ≥ 1806 MPa ($n = 14$) and values ≤ 1806 MPa ($n = 10$) in the transverse orientation. The mean UTS for H3 is similar to H2 and H4 but H1 has a lower mean than the other three Heats in each orientation. There is a strong positive correlation between the TYS and UTS data with a correlation coefficient of 0.91.

The results show that H2, H3, and H4 have consistent tensile strengths with similar means. H3 has a significantly larger standard deviation than H2 and H4. All the H3 tensile coupons were heat treated in the same batch and there is no correlation between location within the billet and strength. A possible explanation for the greater standard deviation and a bimodal distribution is variation in furnace heat zone during aging or variation in furnace set point during aging. However, the difference in means of the two H3 distributions is small (between 22.7 and 28.2 MPa). It is unlikely that metallographic or fractographic analysis of the failed samples would reveal any differences due to the observed small differences in the mean values. A normal distribution can still be used to describe all 24 longitudinal or transverse coupons of H3.

H1 shows a lower distribution for tensile strength than the other three Heats. H1 has the largest billet diameter and the smallest reduction ratio from the 30" ingot. Less strain is imparted during press forging of the larger H1 billet, possibly accounting for the small reduction in strength. However the decrease in TYS and UTS between H1 and the other Heats is sufficiently small for the difference to be considered within expected variance Heat to Heat.

The observations from the four Heats were combined into a single dataset. For the 92 observations for each grain orientation, the following are the average values and the sample standard deviations: longitudinal TYS of 1723 ± 13 MPa, transverse TYS of 1722 ± 12 MPa, longitudinal UTS of 1812 ± 12 MPa, and transverse UTS of 1809 ± 11 MPa. There is no significant difference in average TYS and UTS for the different grain-orientated specimens (from ANalysis Of Variance (ANOVA) at the 5% level).

3.2 Compressive and Shear Strength

The average compressive yield strength (CYS) at 0.2% for the 13 test coupons is 1817 ± 43 MPa. There is a linear positive correlation between the average CYS and TYS values for each heat. A decrease in TYS of 25 MPa corresponds with a decrease in CYS of 100 MPa. This results in the large standard deviation for the average CYS.

The average shear strength (SS) for the 10 test coupons is 1118 ± 6.5 MPa. The range of SS test data is only 21 MPa, 1.9% of the average value.

3.3 Ductility

The distribution of % ELongation (%EL) data of the 184 coupons separated by Heat and grain orientation is plotted in Fig. 4. The bin size is 0.4%. For all four Heats in the longitudinal and transverse directions the %EL data appear to follow a normal probability distribution. H4 shows a slightly lower mean %EL in both orientations compared with the other three Heats. The %EL standard deviations for each orientation and Heat are all similar, in contrast to the tensile properties. Although H4 has the highest average longitudinal UTS, H2 has a similar average value and the largest average longitudinal %EL. A similar observation can be made by comparing the transverse datasets. H2 has the highest average transverse UTS and a greater average transverse %EL than H4. For the data presented here, there is no correlation between UTS and %EL, and no correlation between ductility and billet diameter.

The distribution of % Reduction in Area (%RA) data for the 184 coupons separated by Heat and grain orientation is plotted in Fig. 5. Again, for all four Heats in the longitudinal and

Fig. 4 Distribution of %EL data from the four Heats of C465: (a) longitudinal and (b) transverse

Fig. 5 Distribution of %RA data from the four Heats of C465: (a) longitudinal and (b) transverse

transverse directions the %RA data approximately fit the normal probability distribution. There is a positive correlation between the distribution of %EL and %RA. For example the distribution of H4 transverse data is shifted lower to the other three Heats for %EL and %RA. The average %RA between the four Heats shows good reproducibility and similar standard deviation. As with %EL there is no correlation between tensile strength and %RA, and between %RA and billet size.

For H3, the mean %EL and %RA for the two separate distributions observed in the tensile distributions ($n = 10$ and $n = 14$) in Fig. 2 and 3 (per grain orientation) were calculated. There is no difference between the two %EL means in the longitudinal orientation and a very small difference of 0.39% in the transverse orientation. For %RA, there is a small difference between the two means of 1.4% in the longitudinal orientation and 1.07% in the transverse orientation. These small differences are within expected variation and are not significant.

Combining the observations from the four Heats, the following are the average values and the sample standard deviations from 92 observations: longitudinal %EL of $10.33 \pm 0.47\%$, transverse %EL of $9.86 \pm 0.49\%$, longitudinal %RA of $55.6 \pm 1.9\%$, and transverse %RA of $51.5 \pm 1.8\%$. In contrast to the tensile strength, the difference in ductility between longitudinal and transverse grain-orientated coupons is significant (from ANOVA at the 5% level). Transverse grain orientation has a lower ductility.

3.4 Fracture Toughness (K_{IC})

The average K_{IC} values for the 21 tests per grain orientation in the longitudinal and transverse orientations are 83.9 ± 4.5 and 83.4 ± 6.8 MPa $\sqrt{m}$, respectively. There is no effect of grain orientation on the combined average value of K_{IC} , although the standard deviation is greater for the transverse orientation. Figure 6 presents the relationship between K_{IC} and

Fig. 6 The K_{IC} for each coupon vs. the average TYS for each Heat and grain orientation for C465

average TYS values for each Heat of C465 and grain orientation. H2, H3, and H4 show a tight distribution of K_{IC} values. H1 has similar K_{IC} values but at a lower mean TYS as observed in Fig. 2 ANOVA was conducted hypothesizing that there is no correlation between K_{IC} and TYS at the 5% level. The hypothesis is not rejected at the 5% level for the longitudinal K_{IC} values. The hypothesis is rejected at the same level for the transverse K_{IC} values but this is due to the high influence of H1. Removing H1 from the analysis reverses the result of the test. This analysis indicates that over this small range of TYS (1705 to 1731 MPa) K_{IC} can be considered to be independent of TYS and billet diameter.

3.5 Salt Spray Corrosion

The minimum time to observe a corrosion point with red rust was 240 h from a H1 coupon. The red rust was only staining on the surface with no evidence of a pit. The most extensive corrosion was after 312 h on a H2 coupon (Fig. 7a) although the corrosion was over less than 1% of the surface. Six coupons were left in the test cabinet for 1000 h, of which five coupons showed no corrosion and the other a single corrosion point with red rust. The ASTM B117 test on 300M cones results in over 80% of the surface covered in red rust after 200 h (Fig. 7b).

Fig. 7 Salt spray corrosion results: (a) H2 coupon after 312 h exposure and (b) 300M cones after 200 h exposure (courtesy of Carpenter Technology)

Fig. 8 Maximum true strain vs. cycles to failure for the 120 C465 coupons tested at $R\epsilon = -1.0$, 0.1 , and -2.0

3.6 Stress Corrosion Cracking (K_{ISCC})

The RSL procedure was first used to determine a K_{IC} value for the 3.5" bar. An average of three samples gave $90.8 \text{ MPa} \sqrt{\text{m}}$, slightly higher than the average from the four Heats. The K_{ISCC} value from two RSL tests polarized at -0.350 V (SCE) was $82.4 \text{ MPa} \sqrt{\text{m}}$ (91% of K_{IC}).

3.7 Grain Size

A total of 24 coupon test results were obtained from H1 to H4 combined. No variation in grain size was found across the four Heats. Also, no variation in grain size between the core of a billet and the outer radius was found. From the combined H1 to H4 grain size measurements, the average ASTM grain size was 5.4, from a data range between 5 and 6.

3.8 Fatigue

Figure 8 presents the strain-controlled fatigue data from all four Heats with maximum strain plotted against cycles to failure. The data are subdivided into datasets representing the three different strain ratios tested; the symbols detail the Heat and grain orientation. Arrows indicate a run-out specimen that did not fail after 10^6 cycles. For a given maximum strain, the order of fatigue resistance with strain ratio is $0.1 > -1 > -2$. At each target life and strain ratio, the data from the four Heats are randomly distributed with no systematic difference across the four Heats.

The majority of strain-controlled fatigue tests were carried out using longitudinal grain-orientated coupons. As with other material properties, the grain orientation may affect the fatigue life of C465. At $R\epsilon = -1.0$, the grain orientation of the coupons has no effect on fatigue life. At $R\epsilon = 0.1$, the transverse coupons tested at 0.009 - 0.01 strain show a slightly lower fatigue life. However, across the whole dataset there is no systematic difference between the fatigue life of longitudinal and transverse grain-orientated coupons. The analysis was carried out using all the data collected; 120 C465 observations including three run outs.

Fractography of the fracture surface was done on the 117 observations that failed. Of those, 109 observations showed initiation at a cleavage zone, a titanium carbonitride particle (Ti-C-N), or an initiation attributed to both. The remaining eight coupons did not show any clear features. The primary initiation site was found to be the edge of the specimen except

for six coupons where initiation was sub-surface. The sequence of failure for the coupons was fatigue mode then static mode. Examples of initiation at a cleavage zone and Ti-C-N particle are shown in Fig. 9 along with the accompanying EDX spectra of the particle.

Figure 10 details the type of initiation of the fatigue crack for the 109 C465 coupons vs. fatigue life. In general, multiple initiation points are prevalent at lower life ($< 10,000$ cycles). Two coupons tested at very high strain failed by single initiation at a cleavage zone. There is an increasing prevalence of initiation starting at Ti-C-N particles (either isolated or part of a cleavage zone) at longer life. For coupons with a negative $R\epsilon$, initiation at a cleavage zone is dominant below 10,000 cycles. However, at $R\epsilon = 0.1$, all three types of initiation seem equally probable below 100,000 cycles with only Ti-C-N initiation occurring above 100,000 cycles.

The size of cleavage zones and Ti-C-N particles was recorded for the 109 observations. There are 44 fatigue coupons where initiation was associated with a Ti-C-N particle. The average particle area is $133 \mu\text{m}^2$ with all particles having a diamond geometry. The range of particle area is large from a few μm^2 to hundreds of μm^2 representing a change in length scale from 1 to $\sim 20 \mu\text{m}$. A three-factor ANOVA was conducted to test the hypothesis that there is no significant difference, at the 5% level, in the main effect of the three factors: billet size, life, and strain ratio on the size of Ti-C-N particles. Table 3 summarizes the analysis. The null-hypothesis is not rejected for the main effect of life, and strain ratio. The hypothesis is rejected at the 5% level for the main effect of billet size. By inspection the mean of the H1 sample is significantly different to the other three. Repeating the three-factor ANOVA with the H1 coupons removed results in accepting the hypothesis for all three main effects (Table 3).

There are 78 fatigue coupons where initiation was associated with a cleavage zone. For coupons where multiple cleavage zones were identified an average size was calculated for the coupon. Cleavage zones are typically rectangular with an average area of 0.012 mm^2 . The range of cleavage zone area is large, $480 \mu\text{m}^2$ to 0.076 mm^2 . A three-factor ANOVA was conducted to test the hypothesis that there is no significant difference, at the 5% level, in the main effect of the three factors billet size, life, and strain ratio on the size of the cleavage zone. Table 3 summarizes the analysis. The hypothesis, that there is no difference in means across billet size, life, or strain ratio is not rejected at the 5% level.

4. Data Analysis

4.1 Static Allowables

The calculation of material allowables requires a minimum amount of test data to ensure reliability specified in the Metallic Materials Properties Development and Standardization (MMPDS) handbook (Table 9.2.4, Ref 16). Depending on the number of coupons and number of Heats/Lots of test data, the A- or S-basis allowables can be calculated. Section 9.2.4 in Ref 16 details the data requirements to calculate allowables for the various material properties data gathered for C465.

The four Heats of C465 managed by the AMRC are equivalent to four Heats as defined by MMPDS; they were produced from four different molten metal sources. Each Heat

Fig. 9 SEM images of the fracture surface of two longitudinal H4 Coupons: (a) and (b) crack initiation at a single cleavage zone, (c) and (d) crack initiation at a single non-metallic inclusion, and (e) EDX analysis of particle in (d) showing it is rich in Ti, N, and C

was processed differently to produce different diameter billets. Subsequently each Heat is also one Lot. There are sufficient test data to calculate the S-basis allowable for the TYS and UTS. It is strongly recommended that to calculate the A-basis allowables, the dataset should be 10 results each from 10 Lots. Although the C465 dataset is only over 4 Lots the A-basis allowables were calculated for TYS and UTS for comparison with the S-basis (see below). The remaining datasets fall below the requirements needed to calculate the S-basis.

The calculation of the S-basis allowable in this report is stated in section 9.4.1 in Ref 16. It assumes the distribution of values is normal, and that 99% of the population is expected to equal or exceed the S-basis minimum value with a confidence of 95%. The one-sided tolerance limit factor for 92 test coupons (from the four Heats with a single grain orientation) is 2.70

from Table 9.10.1 in Ref 16. For datasets below the minimum requirements defined by MMPDS, the empirical (mean-3-sigma) rule is used to calculate the allowable. This provides some statistical assurance for the value.

Table 4, column 3 presents the allowables for the C465 static properties tested in this campaign. The table includes the allowables listed for C465 in the MMPDS handbook in column 4 from Tables 2.6.5.0(a) and (d) in Ref 16. The A-basis allowables calculated using the sequential Weibull procedure for TYS and UTS properties are presented in column 5.

4.2 A-Basis Allowable Calculation

One methodology to calculate the A-basis allowable for the tensile properties is the sequential Weibull procedure. Firstly,

the transverse and longitudinal TYS and UTS datasets were separately fitted to the three-parameter Weibull distribution without censoring the dataset detailed in section 9.5.4.7 in Ref 16. The allowables were then calculated using the sequential Weibull procedure (section 9.5.5.2, Ref 16). The criterion to validate that the A-basis value can be computed from this dataset is the “Anderson-Darling” test for Weibullness (section 9.5.4.7, Ref 16). The “Anderson-Darling” test for normality was also carried out for comparison (section 9.5.4.1, Ref 16).

For the tensile properties, the distributions of the combined test results (92 in each grain orientation) are plotted in the form of histograms in Fig. 11 and Fig. 12. Each figure includes the normal and three-parameter Weibull probability density function (pdf) curves. The three parameters of the Weibull distribution were estimated using the procedure described in Ref 16. The population mean and variance of the normal distribution were estimated from the sample mean and variance. For the four datasets, the normal and three-parameter Weibull pdf have been calculated.

The “Anderson-Darling” test for normality and Weibullness was performed on the four datasets. The null-hypothesis, at 5%

error, is that the population follows a normal or three-parameter Weibull population.

The longitudinal TYS dataset can be fitted to the normal and three-parameter Weibull pdf curves (Fig. 11). The transverse TYS dataset has a lower tail and is described better by the three-parameter Weibull pdf (Fig. 11); the dataset fails the null-hypothesis for normality. Both UTS datasets can be fitted to the normal and three-parameter Weibull distributions (Fig. 12). The three-parameter Weibull pdf describes positive skewness for the longitudinal dataset, whereas the transverse dataset shows negative skewness. This skewness is not significant as both datasets pass the test for normality.

5. Fatigue Allowables

5.1 Derivation of Fatigue Curves

There are sufficient data at all three strain ratios to satisfy the requirements in Table 9.2.4 in Ref 16. In order to derive best-fit fatigue curves, data were taken from the cyclic stress-strain curve at a specimen's half-life or 85,000 cycles, whichever was earlier. This was typically above 100 cycles at which stage the cyclic stress-strain curve has reached a steady-state (Ref 17). The analysis uses true strains and stresses.

The methodology used to calculate best-fit strain-life ($E-N$) curves is described in sections 9.6.1 and 9.9.1 in Ref 16. A best-fit equivalent strain model was employed (Eq. 9.6.1.4(c), Ref 16) and the parameters A_1 , A_2 , and A_3 were optimized (A_4 was set to 0). The model was fitted to 117 observations; two run-out observations were excluded during parameter optimization (section 9.6.1.5, Ref 16) and one observation was classified as an outlier (section 9.6.1.6, Ref 16).

Then the cyclic stress-strain curve and mean stress relaxation behavior of C465 were derived to produce individual best-fit $E-N$ curves for each set of strain ratio data. The cyclic stress-strain relationship was then used to convert the best-fit $E-N$ curve to a best-fit stress-life ($S-N$) curve.

Fig. 10 The mechanisms of fatigue failure for the 109 C465 coupons vs. life

Table 3 ANOVA analyses to determine the main effect of billet size, life, and strain ratio on the size of Ti-C-N particles and cleavage zones in C465

Source	Sum of squares	Degrees of freedom	Mean squares	F ratio	P value
<i>ANOVA for 44 Ti-C-N particles from H1 to H4</i>					
Heats	115,880.1	3	38,626.7	3.02	0.0426
Life	59,956.6	3	19,985.5	1.56	0.2158
Re	5168.5	2	2584.2	0.2	0.818
Error	447,656.4	35	12,790.2		
Total	604,242.8	43			
<i>ANOVA for 32 Ti-C-N particles from H2, H3, and H4</i>					
Heats	51,320.1	2	25,660	1.58	0.2263
Life	8268.9	2	4134.4	0.25	0.7775
Re	42,852.6	2	21,426.3	1.32	0.2857
Error	406,540.4	25	16,261.6		
Total	490,503.7	31			
<i>ANOVA for 78 cleavage zones from H1 to H4</i>					
Heats	6.85×10^{-4}	3	2.28×10^{-4}	1.19	0.3208
Life	1.68×10^{-4}	3	5.61×10^{-5}	0.29	0.8311
Re	1.91×10^{-4}	2	9.57×10^{-5}	0.5	0.6099
Error	1.33×10^{-2}	69	1.92×10^{-4}		
Total	1.45×10^{-2}	77			

Table 4 Summary of static allowables for C465

Property	Orientation	CREST C465	C465 (Ref 16)	C465 sequential Weibull procedure
Ultimate tensile strength (MPa)	Long.	1778(d)	1654.8(e)	1785(f)
	Trans.	1778(d)	1654.8(e)	1761(f)
Tensile yield strength 0.2% (MPa)	Long.	1688(d)	1516.9(e)	1671(f)
	Trans.	1688(d)	1516.9(e)	1670(f)
Compressive yield strength 0.2% (MPa)	Long.	1686(c)	1606.5(f)	-
Shear strength (MPa)	Long.	1098(c)	923.9(f)	-
% Elongation (5D basis)	Long.	8.9(c)	10(d)	-
	Trans.	8.4(c)	8(d)	-
% Reduction in area	Long.	49.9(c)	45(d)	-
	Trans.	46.1(c)	35(d)	-
K_{IC} (MPa $\sqrt{m}$)	Long. L-C	70.5(c)	not stated	-
	Trans. C-L	63.0(c)	not stated	-
K_{ISCC} (MPa $\sqrt{m}$)	Long. L-C	82.4(a)	not stated	-
	Trans. T-L $\ddagger$	not tested	74.7(b)	-
Salt spray corrosion (h)	Trans.	240(a)	200I(b)	-
Grain size (ASTM E112)	N/A	5.4(b)	not stated	-

(a) Minimum value

(b) Nominal value

(c) Allowable based on $\mu-3s$

(d) S-basis allowable

(e) Specification minimum

(f) A-basis allowable

$\ddagger$ Double cantilever beam, wedge loaded, constant immersion in 3.5% NaCl (pH 6) per NACE Standard TM0177-96. Specimens loaded in the transverse grain orientation with crack growth in the longitudinal grain direction

Fig. 11 The distribution of the TYS dataset overlaid with normal and three-parameter Weibull pdf curves: (a) longitudinal and (b) transverse

The cyclic stress-strain curve is described by the following equation [17]:

$$\varepsilon_a = \frac{\Delta \varepsilon}{2} = \frac{\Delta \varepsilon_e}{2} + \frac{\Delta \varepsilon_p}{2} = \frac{\Delta \sigma}{2E} + \left(\frac{\Delta \sigma}{2K'} \right)^{1/n'} = \frac{\sigma_a}{E} + \left(\frac{\sigma_a}{K'} \right)^{1/n'} \quad (\text{Eq 1})$$

The parameters E , K' , and n' were calculated by linear least-squares regression on the separated elastic Eq 2 and plastic components Eq 3 of the total strain.

$$\frac{\Delta \sigma}{2} = E \frac{\Delta \varepsilon_e}{2} \quad (\text{Eq 2})$$

Fig. 12 The distribution of the UTS dataset overlaid with normal and three-parameter Weibull pdf curves: (a) longitudinal and (b) transverse

$$\log \left[\frac{\Delta \sigma}{2} \right] = n' \log \left[\frac{\Delta \varepsilon_p}{2} \right] + \log K' \quad (\text{Eq 3})$$

Figure 13(a) presents the derived cyclic stress-strain curve.

There are two mean stress relationships to derive for strain ratios other than -1.0 . The mean stress relaxation relationship at high strain ranges and the elastic mean stress response at low strain ranges. A weighted linear least-squares regression analysis was carried out to derive a relationship between mean stress during relaxation and strain amplitude at the strain ratios 0.1 and -2.0 (section 9.9.1, Ref 16). The weighting emphasized results at high strain range. A cut-off was used with test

data with $\Delta\epsilon_p < 0.01\%$ discarded as being within the elastic mean stress response.

The elastic mean stress response is defined by the parameter β_3 (section 9.9.1, Ref 16) which describes the gradient of the elastic response of mean stress with strain amplitude Eq 4 using the value of E from Eq 2, and an offset (β_{Offset}) calculated from linear least-squares regression. A maximum ϵ_a of 0.32% and 0.52% for strain ratios 0.1 and -2.0 was applied above which stress relaxation occurred.

Fig. 13 (a) C465 cyclic stress-strain plot with best-fit curve and (b) C465 linear models of the change in mean stress for increasing strain amplitude at the three strain ratios

$$\sigma_m = \beta_3 \epsilon_a + \beta_{\text{Offset}} \quad \text{where} \quad \beta_3 = \left[\frac{1 + R\epsilon}{1 - R\epsilon} \right] E \quad (\text{Eq 4})$$

At $R\epsilon = -1.0$, the mean stress is independent of the strain, averaging -40.2 MPa. Figure 13(b) shows the modeled mean stress relationships for the three strain ratios. The correlative information to produce the individual best-fit E - N curves is tabulated in Table 5.

5.2 Best-Fit E - N and S - N Curves

Figure 14 plots the best-fit E - N curves (strain amplitude versus life) for each strain ratio tested alongside the 120 observations. The three best-fit curves show good fit to the observations. The curves show typical features. At high strain range, the curves converge as plastic strain is large and mean stress is fully relaxed during cycling approaching zero. At low strain range the individual strain ratios assume their elastic mean stress response and diverge from each other. This is most prominent in the $R\epsilon = 0.1$ dataset.

Figure 15 plots the S - N relationship, using cyclic stress-strain equation (Table 5) to calculate the stress amplitude curves and data from the best-fit strain amplitude curves and data in Fig. 14.

5.3 Statistical Assurance: One-sided Lower Tolerance Curves

A single straight line is defined such that the probability is at least 95% that at least 95% of the proportion (P) of future observations will lie above this line i.e., 95% probability of survival with 95% confidence. A procedure to calculate the lower tolerance curve is described in Ref 18 assuming the

Table 5 Correlative information to assemble C465 E - N and S - N best-fit fatigue curves

Product form:	Test parameters:
Carpenter custom 465®	Strain rate/frequency
Production: 4 Heats (30 inch round) by the VIM-VAR process.	max 1 Hz strain control
Press forged: 12.5, 10.5, 8.5, & 6.5 inch rounds	max 10 Hz load control after 85,000 cycles
Grain orientation: longitudinal (Long.) or transverse (Trans.)	Wave form-sinusoidal
Thermal mechanical processing history:	Temperature-ambient
Annealed at: 982 °C (1800 °F) for 1 h; Oil quench; sub-zero treatment at -73 °C for 8 h	Atmosphere-air
	No. of Heats/lots: 4
Aged at: 510 °C (950 °F) for 4 h; air cool H950 condition	Equivalent strain equation:
	$\log N = -8.55 - 5.91 \log [\epsilon_{\text{eq}}]; \epsilon_{\text{eq}} = \Delta\epsilon^{0.490} [\sigma_{\text{max}}/E]^{(1-0.490)}$
Stress-strain equations:	Standard deviation $\log(N)$:
Cyclic: $\epsilon_a = \frac{\sigma_a}{200015} + \left(\frac{\sigma_a}{2375}\right)^{1/0.0705}$	$1.17 \left(0.0170 + 0.000851 \frac{1}{\epsilon_{\text{eq}}}\right)$
Sample size: 120	Standard deviation of the fit: 0.161
Mean stress relaxation:	Lower tolerance equation:
$R\epsilon = 0.1$ $\sigma_m = 1156 - 121,600(\epsilon_a)$	$\log N = -8.55 - 5.91 \log [\epsilon_{\text{eq}}] - 2.30[\text{SD}]$
$R\epsilon = -2.0$ $\sigma_m = -705.8 + 64,390(\epsilon_a)$	Adjusted R^2 statistic: 98.6%
$R\epsilon = -1.0$ $\sigma_m = -40.2$	Elastic mean stress response:
Specimen details:	$R\epsilon = 0.1$ $\sigma_m = 244,463(\epsilon_a) - 21.0$
	$R\epsilon = -2.0$ $\sigma_m = -66,672(\epsilon_a) - 12.5$
	Uniform gage test section 7.98 mm diameter
	Polished to $Ra = 0.16$ μm
	Stress relieved
	Residual stress: compression from x-ray diffraction

Fig. 14 Best-fit $E-N$ curves for the C465 fatigue data for each strain ratio, computed from the equivalent strain model (in Table 5), mean stress relaxation model, and cyclic stress-strain curve (Fig. 13)

Fig. 15 Best-fit $S-N$ curves for the C465 fatigue data for each strain ratio, using the cyclic stress-strain relationship (Fig. 13a) to convert from strain amplitude to stress amplitude

observations are normally distributed about the regression line (see Appendix).

Figure 16 presents the best-fit and lower tolerance limit (LT) $S-N$ curves for the three strain ratios tested. One $R\epsilon = 0.1$ transverse observation out of a total of 120 lies below the lower tolerance $S-N$ curve. The non-constant variance in the observations is visible by the increase in the separation between the best-fit and LT $S-N$ curves. As the LT curves are calculated using the equivalent strain, the offset to the best-fit curve is independent of strain ratio. The offset increases in a non-linear manner with longer life, as expected from the increased scatter in observations. At a typical life of a landing gear ($\sim 10^5$ cycles), the offset is a factor of 3.2. To simplify the relationship between the two curves, the standard deviation of the equivalent strain fit can be used from Table 5 giving a constant factor of 2.35 on fatigue life.

6. Discussion

Overall C465 shows very good consistency in material properties between billets of different diameters. This provides confidence that the material properties of a C465 main fitting forging will be invariant across the component. The bimodal distribution for H3 and the lower tensile properties of H1 are

Fig. 16 Best-fit and lower tolerance $S-N$ curves for the C465 fatigue data

not considered significant. The small range of tensile strength covered by the four Heats explains the lack of correlation between tensile and ductility properties (Fig. 2 to 5) as well as between K_{IC} and TYS (Fig. 6). A correlation between TYS and CYS for C465 is observed with the change in CYS four times greater than the change in TYS. This explains the greater spread in the average CYS values for the four Heats.

6.1 C465 Static Material Allowables

The strength of the CREST C465 is greater than the values stated in Ref 16 for the various modes tested. This is attributed to tightly controlling the titanium content in the alloy and significantly lower residual levels compared with the specification (Table 1). This could also be the cause of the elongation to failure in the longitudinal direction being below the S-basis allowable in Ref 16. The %RA shows an improvement over the MMPDS value. The fracture toughness remains high despite the increase in strength; the nominal transverse ($T-L$) K_{IC} quoted in the C465 datasheet is $93.4 \text{ MPa}\sqrt{\text{m}}$ [19]. The general corrosion resistance of the CREST C465 is above the minimum specified in Ref 16. Also, the general corrosion observed has been limited to a single point or small area of a coupon indicating very good corrosion resistance. K_{ISCC} of C465 determined by CRS is also very good with values of 91% of the K_{IC} value under freely corroding conditions in 3.5% NaCl. 300M and 35NCD16THQ have poor K_{ISCC} resistance in 3.5% NaCl (Ref 1, 2).

A parametric representation is possible for the longitudinal and transverse TYS and UTS datasets, either normal or Weibull. The S-basis material allowables for TYS and UTS calculated here can be submitted to the MMPDS. The A-basis material allowables could be provisionally accepted and updated when further Lots of the material are tested. With the exception of the longitudinal UTS dataset, the A-basis allowables calculated using the sequential Weibull procedure are conservative compared with the S-basis allowables assuming a normal distribution. However, the difference between S- and A-basis is small at 17-18 MPa, a 1% decrease, and the A-basis allowable is still a significant increase on the allowables quoted in the MMPDS handbook. The higher A-basis allowable for the longitudinal UTS compared with the S-basis allowable stems from there being no discernable lower tail in the distribution (Fig. 12), which is reflected in the Weibull pdf curve not the normal pdf curve. In this case, the S-basis allowable would be more representative for future use.

Table 6 The relative difference in key material properties between C465 and the current UHTS steels used in landing gear applications

Property	Orientation	C465 (16) vs. 35NCD16THQ	CREST C465 vs. 35NCD16THQ	CREST C465 vs. 300M
Tensile yield strength (0.2%)	Long./trans.	104.6%	116.4%	106.2%
Ultimate tensile strength	Long./trans.	91.9%	98.8%	92.1%
% Elongation	Long.	142.9%	127.2%	127.2%
	Trans.	133.3%	139.6%	209.4%
% Reduction in Area	Long.	112.5%	124.9%	199.8%
	Trans.	116.7%	153.3%	230.0%
Fracture toughness (K_{IC})	Long.	Not specified	94.1%	128.3%
	Trans.	Not specified	84.0%	114.5%

Fig. 17 Comparison of the C465 and 35NCD16THQ S-N curves

Table 6 compares the key CREST C465 material allowables relative to the minimum specification for 35NCD16THQ approved by MBD and 300M from Table 2.3.1.0(f1) in Ref 16. The material allowables of CREST C465 are an improvement over the UHTS steels apart from the UTS. Although the fracture toughness is slightly lower compared with 35NCD16THQ, i.e., 6 and 16% for the longitudinal and transverse orientations, respectively, importantly it is significantly higher than 300M which is regarded as a benchmark for landing gear applications. The discrepancy in UTS against 35NCD16THQ is small at 1.2%, with the UTS of the CREST C465 a 6.9% improvement over the MMPDS value. CREST C465 has a greater deficiency in UTS to 300M of 7.9%.

6.2 C465 Fatigue Allowables

The three derived equations describing the $E-N$ curve, for the three strain ratios, successfully model the C465 H1 to H4 fatigue data. Hence the S-N curves also successfully model the data by virtue of an excellent fit to the derived cyclic stress-strain curve (Fig. 13a).

Figure 17 compares the C465 S-N curves at $R\epsilon = -1.0$ and 0.1 with 35NCD16THQ S-N curves derived from a separate test campaign, using the procedure in Ref 16. Test coupons were extracted from two Aubert and Duval main fitting forgings (2 Heats of material) and tested at the same testing house to provide a like-for-like comparison. Table 7 presents the correlative information to produce the individual best-fit $E-N$ and S-N curves. For the $R\epsilon = -1.0$ condition, C465 and 35NCD16THQ have similar fatigue performance at high strain ranges. Above a life of 1000 cycles there is an increasing

abatement in the fatigue life of C465 compared with 35NCD16THQ; the percentage decrease in the allowed stress amplitude to achieve a fatigue life of 10^5 is 19.3%. For the $R\epsilon = 0.1$ condition, the abatement in fatigue life of C465 compared with 35NCD16THQ increases from 3.3% at 1000 cycles reaching a maximum of 31.5% at 20,000 cycles. At this point, C465 adopts its individual elastic stress behavior and the abatement in life slowly falls reaching 27.6% at 100,000 cycles.

6.3 Fractography

Initiation of failure of C465 fatigue test coupons studied here is dominated by surface initiation either through a cleavage zone or a Ti-C-N particle. Multiple initiations are prevalent at higher cyclic strains where the coupon undergoes plastic deformation during testing.

At small strain ranges fatigue life is dependent on the initiation mechanism at a Ti-C-N particle and crack growth from the particle (Fig. 10). The initiation is typically at a single location, i.e., the most susceptible particle. The significance of Ti-C-N particles on fatigue life is dependent on the strain ratio applied to the test coupon. At $R\epsilon = 0.1$, Ti-C-N particles are responsible for failure an order of magnitude lower than at the other strain ratios. A continuous tensile load on the specimen increases the probability of crack initiation and subsequent growth at a particle.

The distribution of Ti-C-N particle size is not dependent on the reduction ratio from the ingot to the final billet (Table 3). The press forging process does not alter the size or the shape of the particles, which are typically diamond shaped with length 10 μm per side. This is confirmed with the observation that the particle size does not change with strain ratio or the strain range the coupon was tested at.

Initiation by a cleavage zone is more probable at large strain ranges. However, there is no change in the size of the cleavage zone with lower life (Table 3), and thus higher strain amplitude where the mean stress relaxes. The size of the cleavage zone is also not affected by the sign of the mean stress (change in strain ratio). With increasing life, only the dominant initiation mechanism changes, not the size of the cleavage zone or the Ti-C-N particle.

Comparing fatigue lives of C465 with 35NCD16THQ shows that at high strain ranges where cleavage dominates, fatigue life is similar (Fig. 17). The fatigue abatement in C465 versus UHTS steels at longer life can be accounted for by the presence of Ti-C-N particles in the C465 microstructure becoming the dominant initiation site. This is typical for titanium-strengthened PH stainless steels.

Table 7 Correlative information to assemble 35NCD16THQ E-N and S-N best-fit fatigue curves

<i>Product form:</i> 35NCD16THQ Production: 2 Heats cut from 2 landing gear main fittings Grain orientation: longitudinal (long.) or transverse (trans.)		<i>Test parameters:</i> Strain rate/frequency max 1 Hz strain control max 10 Hz load control after 85,000 cycles Wave form-sinusoidal Temperature-ambient Atmosphere-air No. of Heats/lots: 2	
<i>Thermal mechanical processing history:</i> Annealed at: 875 ± 10 °C (1607 ± 36 °F) for 1 h, air cool; Within 2 h, sub-zero treatment at -70 °C (-94 °F) for 2 h minimum Aged at: 210 ± 5 °C (410 ± 18 °F) for 8 h, air cool.		<i>Equivalent strain equation:</i> $\log N = -10.1 - 6.92 \log [\epsilon_{eq}]; \epsilon_{eq} = \Delta \epsilon^{(1-0.545)} [\sigma_{max}/E]^{(1-0.545)}$	
<i>Stress-strain equations:</i> Cyclic: $\epsilon_a = \frac{\sigma_a}{202112} + \left(\frac{\sigma_a}{2874}\right)^{1/0.113}$		Standard deviation log(N):	$0.977 \left(0.00193 \frac{1}{\epsilon_{eq}}\right)$
<i>Sample size:</i> 76 <i>Mean stress relaxation:</i> Re = 0.1 $\sigma_m = 886.1 - 81,760(\epsilon_a)$		Standard deviation of the fit:	0.245
		Lower tolerance equation:	$\log N = -10.1 - 6.92 \log [\epsilon_{eq}] - 2.46[SD]$
		<i>Adjusted R² statistic:</i> 96.2%	
		<i>Elastic mean stress response:</i>	
		Re = 0.1	$\sigma_m = 247,025(\epsilon_a)$
		Re = -1.0	$\sigma_m = -56.7$
<i>Specimen details:</i>		Uniform gage test section 7.98 mm diameter Polished to Ra = 0.16 µm Stress relieved Residual stress: compression from x-ray diffraction	

6.4 C465 in Landing Gear Applications

The CREST programme targets C465 for the A320 family aircraft main landing gear main fitting currently manufactured from 35NCD16THQ. From a technical performance standpoint, the tighter specified C465 is an excellent stainless steel to replace 35NCD16THQ in landing gear applications. C465 static material properties are comparable or superior to 35NCD16THQ with the key benefit of significantly improved corrosion resistance; additional cadmium coating is not required.

The key consideration for C465 to replace 35NCD16THQ is the lower fatigue resistance for the typical life of a landing gear. This is up to 27.6% depending on strain ratio. This can be resolved for the main fitting component by reducing the local stress in the critical fatigue areas. This would involve some re-design, resulting in a small weight increase.

The fatigue abatement of 27.6% considers the difference between best-fit curves with at least 50% of future observations above the line with 50% confidence. The actual abatement for design will be between the lower tolerance limit curve for C465 (at least 95% probability of survival with 95% confidence) and the minimum curve for 35NCD16THQ. Due to the low scatter observed in the fatigue data collected during the test campaign resulting in a tight lower tolerance limit curve for C465, the abatement could be less than 27.6%.

The UTS of C465 falls short of the minimum specification for 300M (Table 6). Therefore, C465 cannot be considered as a direct replacement for 300M components without significant re-design to compensate for the lower strength and fatigue performance.

7. Conclusions

CREST Custom 465[®] showed consistently better tensile, compressive, and shear strength allowables than quoted in

the MMPDS handbook, comparable elongation, and better reduction in area. This higher specification is due to tight control of the titanium content and low residual levels; a continuous evolution for the material since the original MMPDS values were generated. The fracture toughness remains high despite the increase in tensile strength. General corrosion and stress corrosion cracking resistance are very good. Custom 465[®] static properties compare well with the current 35NCD16THQ steel used for the landing gear main fitting.

Low-cycle strain-controlled fatigue tests on Custom 465[®] have been analyzed and strain-life and stress-life curves have been generated to show the fatigue resistance at various strain ratios. There is a decrease in fatigue resistance of Custom 465[®] compared with UHTS steels at longer life. Custom 465[®] fatigue failures are initiated predominately at cleavage zones at high strain ranges or titanium carbonitride particles at lower strain ranges. The abatement of fatigue resistance at long life can be accounted for by the presence of titanium carbonitride particles.

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Appendix: One-Sided Lower Tolerance Curves

The lower tolerance limit confidence level is defined in Eq 5:

$$C = C_2 - \frac{1}{2}(1 - C_1). \quad (\text{Eq 5})$$

Here, both C and C_1 are 95%, and C_2 is 97.5%. The lower tolerance limit is given by Eq 6. The factor C^* is the value that satisfies the double integral expression, Eq 7.

$$\log N = A_1 + A_2 \log(\varepsilon_{\text{eq}}) - \left\{ C^* + z_p / \sqrt{\frac{\chi_{n-2,97.5\%}^2}{n-2}} \right\} \text{SD} \quad (\text{Eq 6})$$

$$C = \int_{-C^*/g}^{C^*/g} \int_{-C^*/h}^{C^*/h} \frac{1}{2\pi\sqrt{1-\rho^2}} \left[1 + \frac{u^2 - 2\rho uv + v^2}{(n-2)(1-\rho^2)} \right]^{-n/2} dv du \quad (\text{Eq 7})$$

The parameters g , h , and ρ in Eq 7 are written as

$$g = \sqrt{\xi_a}, \quad h = \sqrt{\xi_b}, \quad \rho = \frac{\xi_{ab}}{\sqrt{\xi_a \xi_b}} \quad (\text{Eq 8})$$

and

$$\xi_a = \frac{1}{n} + \frac{(\varepsilon_{\text{eq},a} - \bar{\varepsilon}_{\text{eq}})^2}{(n-1)s_{\varepsilon_{\text{eq}}}^2}, \quad \xi_b = \frac{1}{n} + \frac{(\varepsilon_{\text{eq},b} - \bar{\varepsilon}_{\text{eq}})^2}{(n-1)s_{\varepsilon_{\text{eq}}}^2}, \quad (\text{Eq 9})$$

$$\xi_{ab} = \frac{1}{n} + \frac{(\varepsilon_{\text{eq},a} - \bar{\varepsilon}_{\text{eq}})(\varepsilon_{\text{eq},b} - \bar{\varepsilon}_{\text{eq}})}{(n-1)s_{\varepsilon_{\text{eq}}}^2}.$$

The expression within the braces in Eq 6 equals 2.30 for the 117 observations used to fit the model (with $z_p = 1.6449$, $\chi_{117-2,97.5\%}^2 = 87.213$ and $C^* = 0.413$).

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