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**Evaluation of the Fatigue Resistance of
CDA 182 Copper for Use in the
Impurity Study Experiment Fusion
Research Tokamak (ISX)**

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METALS AND CERAMICS DIVISION

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EVALUATION OF THE FATIGUE RESISTANCE OF CDA 182 COPPER
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ABSTRACT

The fatigue behavior of CDA 182, a Cu-1% Cr alloy, was evaluated under load-controlled cycling, simulating service conditions in ISX, a fusion research tokamak. Heat-treated, work-hardened CDA 182 was selected for use in ISX on the basis of relatively good mechanical strength and high electrical conductivity. In fully reversed and zero-to-maximum-tension uniaxial fatigue tests at room temperature, engineering stress amplitude versus number of cycles to failure and cyclic strain behavior were determined for cyclic lives from 2×10^2 to 4×10^6 . Cyclic softening and tensile ratchetting both occurred during fully reversed cycling, but neither occurred in the zero-to-maximum-tension tests. Varying degrees of nonisotropic deformation were observed under both conditions of loading. Increasing the testing temperature to 66°C (150°F) slightly reduced fatigue strength but did not affect the cyclic strain response. In addition to uniaxial tests, prototype specimens of the "weakest link" in the ISX design for CDA 182, an interlocking finger joint, were tested under load control. These tests assessed the degrading effect of stress concentrations in the joint on the fatigue resistance of the material. The prototypic tests simulated both normal and above-normal loading conditions. Details on the finger joint specimen configuration, test results, and fractographic observations on the failure mode are reported. The combined results of both portions of the test program allowed the fatigue stress concentration factor of the joint to be estimated for a range of cyclic lives bounding the design lifetime of the device. Subsequent analysis indicated that a substantial margin of safety exists for expected conditions of service in ISX.

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INTRODUCTION

The potential for fatigue-induced failure in the magnetic containment coils of a fusion research device require the evaluation of fatigue resistance of the coil material. A Cu-1% Cr alloy, CDA 182, has been chosen for this application in the Impurity Study Experiment Research Tokamak (ISX) (refs. 1 and 2) built for the Fusion Energy Division at Oak Ridge National Laboratory. ISX (Fig. 1) uses electromagnetic coils to create a pulsed toroidal magnetic field to contain the plasma.

¹R. O. Hussung et al., "ISX Toroidal Field Coil Design and Analysis," p. 773 in *Proc. 6th Symp. Engineering Problems of Fusion Research*, Institute of Electrical and Electronic Engineers, San Diego, November 1975.

²D. C. Lousteau et al., "Engineering Features of ISX," pp. 366 *Proc. 6th Symp. Engineering Problems of Fusion Research*, Institute of Electrical and Electronic Engineers, San Diego, November 1975.

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Fig. 1. Elevation View of Experiment Fusion Research Tokamak. 15x.

Electromagnetic interactions of the coils causes load-controlled fatigue cycling of the material. The coil load per turn varies between nominally zero and 90 kN (20,000 lb) tensile load an estimated 10^5 cycles. Therefore, the fatigue resistance of CDA 182 was determined under load-controlled cycling. This was done in two ways. First uniaxial testing established the variation of an engineering stress amplitude, σ_A , with number of cycles to failure, N_f . Then limited testing of the structural "weak link" of the removable coil segment, the finger joint (Figs. 2 and 3), assessed the degrading effect of stress concentrations on the fatigue life of the structure. The placement of the coil segments in the actual device is shown in Fig. 4.

Fig. 2. Removable Coil Segment of ISX Showing Finger Joints.

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Fig. 3. Detail of One Plate of ISX Finger Joint [Surface Finish in Micrometers (Micro Inches)].

Fig. 4. ISX Under Construction Showing Removable Coil Segments with Finger Joints.

MATERIAL AND EXPERIMENTAL PROCEDURE

The material, CDA 182, copper alloyed with 1.08% Cr, was selected on the basis of good mechanical strength (Table 1) and high electrical conductivity, 89% IACS. Its product form is 25-mm (1-in.) plate delivered and tested in the hard temper.

Table 1. Mechanical Properties of CDA 182

Property	Longitudinal	Transverse
Yield Strength, 0.5% offset, MPa (ksi)	352 (51)	358 (52)
Ultimate Tensile Strength MPa (ksi)	400 (58)	410 (59.4)
% Elongation in 50.8 mm	23	23
R _B Hardness	71	71

The processing history of the material is as follows: hot roll to 51-mm-thick (2-in.) plate; solution heat treated at 950°C (1742°F) for 1200 to 1800 s (20 to 30 min); water quench to room temperature; temper at 450°C (842°F) for 14,400 s (4 hr); and cold roll to 25-mm (1-in.) plate. This sequence produced both good strength and conductivity in the material.

In the microstructure obtained is shown in (Fig. 5). Small particles of undissolved chromium can be seen elongated along with the copper-rich α matrix during final cold rolling. Also evident is some flattening of the α grains through the thickness of the plate. The shading seen among the α grains is caused by orientation differences.

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MS-76-0818-5

Fig. 5. (a) Plate Surface of CDA 182, Polished and Etched, 500x. Rolling direction indicated by arrow. (b) Section perpendicular to rolling direction with short traverse oriented vertically. 500x.

Uniaxial Fatigue

The specimen used for uniaxial testing (Fig. 6) was an hourglass type with button head ends. Specimens were fabricated with their major areas longitudinal and transverse to the rolling direction of the plate. Final machining on the hourglass portion of the specimen was as follows: (1) rough machine to leave 0.25-mm (0.010-in.) stock on the radius over the final dimension, (2) then remove a maximum of 6 μm (0.0025-in.) of stock material in any pass, (3) buff to a 0.20-0.28 μm (8-11 $\mu\text{in.}$) finish by polishing parallel to the specimen longitudinal axis, and (4) polish reduced area longitudinally to remove all circumferential marks visible at approximately 20X magnification.

The load-controlled testing was done in a servocontrolled electro-hydraulic test frame equipped with hydraulically locking grips and a four-post die set to assure good alignment and system rigidity. Testing was done in air at room temperature both under fully reversed load cycling as well as cycling between zero and maximum load. A few comparison tests were also made at 66°C (150°F). Temperature control for these tests was

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Fig. 6. Hourglass Specimen Used in This Study. All dimensions are in millimeters (inches) except surface finishes, which are in micrometers (microinches).

maintained by electric heat tapes wrapped around the exposed portions of the specimen immediately adjacent to its reduced section. Varying the heat input to the specimen with variable transformers and subsequently letting the specimen equilibrate maintained test temperature within about $\pm 2^{\circ}\text{C}$ ($\pm 4^{\circ}\text{F}$). All tests were begun in tension.

A hinge-type diametral extensometer with an LVDT transducer monitored strain. The output of the extensometer was fed into a strain computer capable of separating the measured total diametral strain, ϵ_d , into elastic and plastic components, ϵ_{de} and ϵ_{dp} , as well as calculating the axial strain, ϵ_a , imposed on the specimen.³ This is done by generating the electrical analogs of the components according to the equations:

$$\epsilon_{de} = -\nu_e F/AE ;$$

$$\epsilon_{dp} = \epsilon_d - \epsilon_{de} ;$$

$$\text{and } \epsilon_a = -\epsilon_{de}/\nu_e - \epsilon_{dp}/\nu_p ;$$

where ν_e and ν_p are the elastic and plastic Poisson's ratios respectively, F is the applied load, A is the cross-sectional area, and E is Young's modulus.

One assumption upon which the accuracy of diametral extensometry depends is that the specimen deforms isotropically. We observed that this was not true for our test material. Depending upon loading conditions, a significant departure from a circular cross section occurred during testing. Hence quantitative strain measurements during testing were unavailable. However, qualitative observations concerning cyclic strain hardening behavior, ratchetting, and shapes of hysteresis loops are still possible.

³T. Slot, R. H. Stentz, and J. T. Berling, "Controlled-Strain Testing Procedures," pp. 100-28 in *Manual on Low Cycle Fatigue Testing*, ASTM Spec. Tech. Publ. 465, American Society for Testing and Materials, Philadelphia, 1969.

In service the coils will experience a 6/10 s load pulse every minute. Initial tests at frequencies of both 1.0 and 0.5 Hz gave no indication of a frequency effect in this limited range. All subsequent testing was done at 1.0 Hz. Uniaxial test conditions and results are summarized in Table 2.

Table 2. Uniaxial Fatigue Testing Conditions

Specimen ^a	Frequency (Hz)	Engineering Stress Amplitude, σ_a		Engineering Mean Stress, σ_m		Number of Cycles to Failure, N_f
		(MPa)	(ksi)	(MPa)	(ksi)	
<u>Tests at 25°C (77°F)</u>						
2T	1.0	327.2	47.46	0	0	2,184
3T	0.5	330.2	47.89	0	0	2,362
4T	0.5	351.5	50.98	0	0	476
5T	1.0	351.9	51.03	0	0	399
6T	0.5	287.3	41.67	0	0	22,462
7T	0.5	258.1	37.43	0	0	71,395
8T	1.0	265.0	38.43	0	0	94,570
9T	1.0	239.6	34.75	0	0	178,686
10T	0.5	242.4	35.16	0	0	225,692
12T	1.0	222.8	32.02	0	0	497,264
1L	1.0	308.8	44.79	0	0	8,297
2L	1.0	286.5	41.55	0	0	18,074
3L	1.0	241.7	35.05	0	0	149,991
4L	1.0	215.7	31.29	0	0	499,908
5L	1.0	345.7	50.14	0	0	830
6L	1.0	167.9	24.35	167.9	24.35	300,882 ^b
7L	1.0	176.6	25.62	176.6	25.62	1,343,047
8L	1.0	193.7	28.10	193.7	28.10	8,534
9L	1.0	183.8	26.67	183.8	26.67	235,407
10L	1.0	188.8	27.38	188.8	27.38	227,783
11L	1.0	196.1	28.44	196.1	28.44	9,620
12L	1.0	192.4	27.91	192.4	27.91	13,280
13L	1.0	182.0	26.39	182.0	26.39	304,513
11T	1.0	170.2	24.69	170.2	24.69	1,226,861
<u>Tests at 66°C (150°F)</u>						
14L	1.0	171.8	24.91	171.8	24.91	309,207
15L	1.0	171.4	24.86	171.4	24.86	400,853
14T	1.0	151.2	21.93	151.2	21.93	3,393,846 ^c
15T	1.0	185.2	26.87	185.2	26.87	48,745

^aT indicates specimen longitudinal axis transverse to the plate rolling direction; L indicates specimen longitudinal axis parallel to the plate rolling direction.

^bFailed prematurely at scratch mark so not used subsequently.

^cDid not fail.

Fatigue Testing of Prototype Finger Joints

In addition to the conventional hourglass specimens, limited testing was done on full scale prototypic finger joints (Figs. 2 and 3). These tests checked the integrity of this portion of the coil segment under simulated fatigue service conditions. In addition, the results would allow an estimation of the fatigue stress concentration factor, K_f , of the finger joint for the test material. In fabricating test specimens, the only changes made from the joint shown in Fig. 3 was the addition of 51-mm (2-in.) shoulders beginning 83-mm (3 1/4-in.) from the reduced section to facilitate specimen loading. A specimen in the test frame and loading fixtures during test is shown in Fig. 7.

Fig. 7. Prototype ISX Finger Joint Specimen During Test. (a) Specimen and gripping assembly in test frame. (b) Closeup of specimen.

Because of the relatively high cost involved, only two specimens were fabricated and tested. Specimen 1 was tested under estimated service loading conditions by cycling between zero and 89.8 kN (20,000 lb) tension at room temperature at 1 Hz. However, at 1,023,015 cycles, with no visible evidence of plasticity or fatigue cracking apparent, the maximum load was raised to 133.4 kN (30,000 lb) for 82,869 cycles. Still no damage was visible, and maximum load was raised to 222.3 kN (50,000 lb).

Failure then occurred after 20,954 cycles, by fracture of the bolts first, and then three fingers (Appendix A). Pictures of specimen 1 after test are shown in Fig. 8. Specimen 2 was tested in the same manner as the first but with a maximum load of 133.4 kN (30,000 lb) throughout the test. After 582,000 cycles with no damage visible, testing was discontinued. Results of tests on the prototypic finger joints are summarized in Table 3.

Photo 169170

Fig. 8. Finger Joint Specimen 1 After Failure.

Table 3. Results of Fatigue Tests on ISX Finger Joints^a

Specimen	Maximum Load, P_M		Maximum Nominal Stress, P_M/Area^b		Number of Cycles at P_M	Total Number of Cycles at or Exceeding P_M
	(kN)	(lb)	(MPa)	(ksi)		
1	89.8	20,000	103.7	15.04	1,023,015	1,126,838
	133.4	30,000	155.6	22.56	82,869	103,823
	222.3	50,000	259.1	37.58	20,954 ^c	20,954
2	133.4	30,000	155.6	22.56	582,000	582,000

^aSpecimens cycled between zero and maximum tensile load at room temperature in air at 1.0 Hz.

^bArea = 1140 mm² (1.33 in.²).

^cFailed.

RESULTS AND DISCUSSION

Uniaxial Testing

Fully reversed load-controlled fatigue tests established a base line with which to compare subsequent effects of mean stress. Fatigue behavior was investigated over the cycle range of 4×10^2 to 5×10^5 in this loading mode. In all cases an examination of the strain record during test revealed both cyclic softening, as evidenced by increased hysteresis loop width, and tensile ratchetting. Cyclic softening occurred from the beginning of all fully reversed tests regardless of whether σ_A exceeded the monotonic yield strength of the material. In general the amount of softening was proportional to the stress amplitude with extensive and softening most evident in tests at the highest stress amplitudes, and decreasing to smaller amounts at lower stress amplitudes. Even in test 4L with $\sigma_A = 216$ MPa (31 ksi) with no plasticity apparent initially in the hysteresis loops, cyclic softening did occur, such that by 30% of the cycle life N_f , some small curvature was obvious in the hysteresis loop.

At the higher stress ranges, some of this apparent strain softening was undoubtedly related to the decrease in cross-sectional area associated with the tensile ratchetting. All other things being equal, the same load

amplitude on a smaller cross section would produce a larger strain amplitude. However, in the tests at the lower stress amplitudes, such as test 4L, the cyclic softening began significantly in advance of any ratchetting, this indicating a change in the mechanical properties of the copper. The degree of work hardening induced in the copper during cold rolling could not be maintained under the fully reversed loading conditions investigated.

Only in tests with significant plasticity from the beginning was ratchetting evident throughout the entire test. For fully reversed loading this generally occurred for σ_A in excess of about 280 MPa (40 ksi).

Since the tensile and compressive load maxima were equal, the engineering stress amplitude was fully reversed. However, dimensional changes in the specimen during the loading cycle would cause a bias in the true stress amplitude. This, in the presence of specimen plasticity, may be responsible for the ratchetting.

From our tests cycled between zero and maximum tensile load, an unbalanced load without plasticity seems insufficient to cause ratchetting in this material. In these tests when the maximum stress exceeded the apparent proportional limit, rapid initial hardening occurred for the first few cycles until a steady-state cyclic strain response was achieved. The plastic strain decreased with each succeeding cycle until the material response appeared totally elastic. During these initial few cycles (usually 10-30) a permanent tensile strain bias developed. However, as the cyclic plasticity diminished so did each succeeding increment of permanent tensile deformation, until both simultaneously reached zero. The degree of this initial instability increased with increasing maximum load, but only in tests 8L, 11L, and 12L, where σ_{max} was within about 8 to 16 MPa (1 to 2 ksi) of the material's ultimate tensile strength, did plasticity and consequent tensile ratchetting persist throughout the test. In zero to maximum load tests in which σ_{max} did not cause any appreciable initial plastic deformation, initial ratchetting was absent as well. The only other situation in which ratchetting occurred was during the final stages of a test after fatigue crack initiation, with an increase in nominal maximum stress proportional to the decrease in load bearing area. This was

also the only portion of the zero to maximum load tests in which any increase in the hysteresis loop width occurred. However, probably dimensional changes caused this apparent increase in strain under these conditions, rather than actual strain softening, which occurred under fully reversed loading.

The four comparison tests at 66°C (150°F) had qualitatively similar strain histories to the zero to maximum load tests at room temperature. These strain histories included: (1) rapid initial cyclic hardening and associated permanent tensile strain bias; (2) an elastic steady-state cyclic strain loop achieved early in the test; (3) tensile ratchetting only in the final stages of the test, caused by increasing true stress on the cracked cross section, with the only exceptions noticed at high maximum stresses; and (4) no apparent cyclic softening at any point during the test. Since absolute strain values were uncertain because of anisotropy, the only quantitative difference in behavior that could be observed was that the load at which ratchetting occurred throughout the test was reduced slightly to 376 MPa (54 ksi) at the higher temperature.

The fatigue life results of all the uniaxial tests (Fig. 9) show no effect of orientation on fatigue life. The results of all fully reversed tests appear to be well represented by a bilinear relationship in semilog space. Copper and copper alloys, unlike numerous ferrous alloys, generally do not exhibit a fatigue limit below which failure does not occur. Hence the lower portion of the fully reversed curve is extrapolated in a simple linear fashion to give a fatigue life of 10^8 cycles at $\sigma_A = 108$ MPa (15.6 ksi). This closely agrees with results of other researchers^{4,5} on work-hardened coppers with similar ultimate tensile strengths (UTS), the mechanical property that can be correlated with high-cycle fatigue resistance.

⁴H. L. Burghoff and A. I. Blenk, "Fatigue Properties of Some Coppers and Copper Alloys in Strip Form," *Proc. Am. Soc. Test. Mater.*, 48: 709-32 (1948).

⁵D. J. McAdam, Jr., "Fatigue and Corrosion-Fatigue of Spring Material," *Trans. Am. Soc. Mech. Eng.*, 51(1): 45-56 (1929).

Fig. 9. Results of Uniaxial Fatigue Tests.

Initially it was thought that the fatigue behavior under mean stress loading could be approximated from the fully reversed results by use of an empirical correction such as the modified Goodman equation.⁶ This proved far too conservative for our test material with such a high yield strength relative to its ultimate tensile strength. The Peterson cubic equation correction,⁷ which has been used with some success on materials with a relatively high yield strength, such as heat treated 4340 bolting material,⁸ was also tried. It more closely approximated the behavior

⁶B. F. Langer, "Design of Pressure Vessels for Low Cycle Fatigue," *J. Basic Eng.* 389-402, (September 1962).

⁷R. E. Peterson, "Brittle Fracture and Fatigue in Machinery," *Fatigue and Fracture of Metals*, John Wiley and Sons, New York, 1952.

⁸A. L. Snow and B. F. Langer, "Low Cycle Fatigue of Large-Diameter Bolts," *J. Eng. Ind.* 53-61, (February 1967).

observed in two earlier verification tests but was still significantly conservative, so more zero-tension tests were done, and Fig. 9 also shows the results.

A bilinear representation also appears to represent the zero-tension data, with the bottom portion fit to the five higher cyclic life data points, and the upper portion fit to the three lower cyclic life data points and the point $N_f = 9$, $\sigma_A = \text{UTS}/2$. This is the number of cycles to failure predicted by extrapolating the upper portion of the fully reversed result to the ultimate tensile strength, and is used to approximate extreme low-cycle fatigue behavior.

The comparison tests at 66°C (150°F) do show a small reduction in fatigue resistance from that observed at room temperature, as can be seen in (Fig. 9). From the limited data available, the fatigue strength appears to be reduced roughly 15 MPa (2.2 ksi) for lives of 3×10^5 or greater, with a decreasing effect at shorter lifetimes.

Coefficients of the equations fit to all the uniaxial data are given in Table 4.

Table 4. Coefficients for the Best Fit Linear Representations, $N_f = A_0 + A_1 \log \sigma_A$, of the Uniaxial Fatigue Results

Load Conditions	N_f Range	Temperature		A_0	A_1
		(°C)	(°F)		
Fully Reversed	<4,857	Room		437.2	-32.15
	>4,857	Room		498.8	-48.86
Zero to max load	< 1.103×10^5	Room		206.4	-3.074
	> 1.103×10^5	Room		273.7	-16.42
66°C (150°F)					
Zero to max load		66	150	268.3	17.66

PROTOTYPE FINGER JOINTS

The fatigue tests of the finger joints gave highly encouraging results when compared with estimated conditions of service. Specimen 1 sustained over 12 times the projected service life of the coils at their design loading with no apparent damage. Specimen 2 sustained over 5 times the projected service life at 150% of design loading also with no visible damage before testing was discontinued. Specimen 1 eventually failed after the maximum cyclic load was raised to 150% of service loading for 82,869 cycles and then finally to 250% for 20,954 cycles. Post-test examinations of its fracture surface indicated that no appreciable fatigue cracking had occurred until the final stage of loading (Appendix A).

A more quantitative assessment can be made of the effects that the stress concentrations in the joint would have on reducing its fatigue resistance from that of a smooth bar cycled between identical nominal stresses by calculating the fatigue stress concentration factor K_f . The fatigue stress concentration factor has been shown to be less than or equal to the theoretical or elastic stress concentration factor K_t .^{9,10} For a given stress riser and material, K_f varies as a function of N_f . At very high cycle lives, K_f may approach K_t but decreases at lower lives (Fig. 10).

In this case the data available for the finger joint are insufficient to allow a complete determination of K_f as a function of N_f . Nevertheless some upper bounds can be established.

In the ISX finger joint the maximum stress will occur at the surface of the inner bolt hole of the right-hand half of the joint shown in (Fig. 3). Tensile stresses at this point will be caused by pin loading in the inner bolt hole and by axial loading from the outer bolt hole. The value of K_t determined at this point can therefore be taken as the maximum value of K_f for the entire finger joint, although it would undoubtedly be unduly

⁹R. C. Juvinall, *Engineering Considerations of Stress, Strain, and Strength*, 249-62, McGraw-Hill, Inc., New York, 1967.

¹⁰B. I. Sandor, *Fundamentals of Cyclic Stress and Strain*, 113-23, University of Wisconsin Press, Madison, 1972.

Fig. 10. Typical Effect of Stress Concentration on Fatigue Life. The fatigue stress concentration factor K_f is the ratio of nominal stress amplitudes of a smooth bar to a notched part respectively, which will yield similar lives. At high cycle lives K_f may sometimes approach the theoretical stress concentration K_t .

conservative at low cycle lives. An exact solution of K_t for this particular configuration is not available; however, its value is assumed to lie between that for the limiting cases of pin loading and axial loading. These have been determined as 2.46 and 2.38 respectively for the geometry in this case. The larger of these will be taken as the absolute upper bound for K_f . To determine if a more accurate upper bound for K_f could be established, the results of the prototype tests given in Table 3 were evaluated.

In the initial portion of loading, specimen 1 sustained 1.023×10^6 cycles without failure at a nominal stress amplitude, σ_{AN} , of 51.8 MPa (7.04 ksi), or one half of the nominal maximum stress. Figure 9 shows a stress amplitude of 175 MPa (25.4 ksi) is necessary to cause failure in that number of cycles for a zero-tension smooth bar test. It is

readily apparent that these results would yield a value of K_f in excess of K_t . Therefore this portion of loading cannot be used to establish a value for K_f at $N_f = 1.023 \times 10^6$, so the value of K_f will be assumed equal to K_t .

However, the final loading stage of specimen 1 can be used to reduce K_f from the value K_t . Considering the significant amount of fatigue the specimen had undergone before that portion of its testing, as well as the fact that the bolts did fail before the fingers, would indicate that the value of K_f derived using $N_f = 20,954$ would still be conservative. Nonetheless it is a more accurate value for K_f than simply using K_t at such a short life. In this case $\sigma_{AN} = 129.6$ MPa (18.8 ksi). The corresponding stress amplitude for smooth bar failure at 20,954 cycles is 193 MPa (28.0 ksi) (Fig. 9); hence $K_f = 193 \text{ MPa} / 129.6 \text{ MPa} = 1.49$ at $N_f = 20,954$. Similar calculations for Specimen 2 yield $K_f = 2.30$ at $N_f = 5.82 \times 10^5$, which again can only be taken as an upper limit since the specimen did not fail. The upper limits of the fatigue stress concentration factor for the ISX finger joint can be summarized:

K_f	N_f
2.46	$>1.023 \times 10^6$
2.30	5.82×10^5
1.49	2.09×10^4

The values of K_f calculated in each case are conservative and may therefore be used to obtain an estimate of the minimum fatigue life which can be expected for the finger joint. To do this the three values of K_f were determined as a function of N_f . Both linear and quadratic approximations were made. The quadratic was the more conservative of the two over the range of life of interest and was used in the subsequent analysis. The equation representing the zero-tension data given in Table 4 were then reduced by the factor K_f given by:

$$K_f = 1.447 + (2.094 \times 10^{-6})N_f - (1.079 \times 10^{-12})(N_f)^2$$

for $N_f \leq 1.01 \times 10^6$,

and $K_f = K_f(\max) = 2.46$ for $N_f > 1.01 \times 10^6$.

This result (Fig. 11) gives the minimum expected life of the ISX finger joint as a function of nominal stress amplitude for zero to maximum load fatigue cycling. For the expected service conditions, 10^5 cycles at $\sigma_{AN} = 51.0$ MPa (7.54 ksi), there is a large margin of safety in both stress amplitude and number of cycles to failure.

Fig. 11. Fatigue Life of CDA 182 at Room Temperature. Smooth bar results from best fit of zero to max load uniaxial data. Finger joint life estimated by reducing the smooth bar best fit by the factor K_f .

CONCLUSION

The fatigue resistance of CDA 182 was tested in two different ways. Load-controlled uniaxial testing on smooth hourglass specimens established fatigue life as a function of engineering stress amplitude for both fully reversed and zero to maximum load cycling. The CDA 182 cyclicly softened and experienced tensile ratchetting during fully reversed cycling under all conditions examined, whereas neither generally occurred in zero to maximum tension loading. Fatigue life as a function of stress amplitude

was substantially smaller for zero to maximum load testing although not to the extent which would generally be predicted from mean stress corrections. A small reduction in high-cycle fatigue life from that observed at room temperature was seen in limited comparison tests at 66°C (150°F).

In addition, limited load-controlled testing of ISX prototype finger joints evaluated the effect of stress concentrations on reducing the fatigue resistance of the material. The results of these tests were used to estimate the fatigue stress concentration factor K_f for the finger joint. Both the results of the finger joints tests themselves and the uniaxial fatigue results when appropriately reduced by K_f indicated substantial margins of safety beyond predicted service conditions.

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APPENDIX

Failure Details of Specimen 1

After the failure of finger joint specimen 1 an effort was made to ascertain the nature of its failure, with particular emphasis on determining in which part of the test fatigue cracks had initiated.

After cycling the specimen between zero and a maximum tensile load of 89.8 kN (20,000 lb) for 1.023×10^6 cycles and then raising the maximum load to 133.4 kN (30,000 lb) for an additional 8.29×10^5 cycles, there was no apparent damage. Neither plasticity nor surface fatigue cracking was evident. The maximum load was then raised to 222.3 kN (50,000 lb) and total separation occurred after 20,954 cycles. The final sequence of macroscopic events leading to separation is as follows, with references made to Fig. A1. The first portions of the joint to fail were the bolts,

Fig. A1. ISX Finger Joint Specimen 1 After Failure.

which sheared between fingers T_5 and B_5 . From this point on, specimen bending became increasingly noticeable, with the T_1 , B_1 side of the specimen acting as a hinge. Fingers B_4 , B_3 , and B_2 then failed in that order, with total separation occurring as the bolts sheared between fingers T_1 and B_1 and fingers T_2 and B_1 .

Macroscopic examination of the fracture surfaces of fingers B_2 , B_3 , and B_4 indicated fatigue cracking partially through the ligament on one side of the bolt followed by ductile rupture (Fig. A2). On the right side of each fracture surface a mostly vertical line exists, most evident on finger B_4 , which is the boundary between fatigue cracking and ductile rupture, with the fatigued portion next to the bolt. The left hand position of each finger failed totally by ductile rupture.

Fig. A2. (a) Top "Hand" of Specimen After Failure Showing Fracture Surfaces of Finger of Bottom "Hand." (b) Detail of (a) fracture surface from bottom to top are of fingers B_2 , B_3 , and B_4 respectively.

The fracture surface of each half of fingers B₂, B₃, and B₄ showing dual failure modes was then examined with a scanning electron microscope. This was done primarily to determine if any sudden increases in fatigue striation spacing had occurred to indicate that crack propagation had been under way when the maximum load was raised. No such increases were observed anywhere on the fatigued surfaces. A slow steady increase in striation spacing was observed at increasing crack length, as would occur in a load-controlled fatigue crack propagation test in which the maximum and minimum loads were held constant throughout the test.

Figure A3 shows the locations of photomicrographs of the fracture surface of finger B₄ and the direction of crack propagation. Figures A4 and A5 illustrate fatigue cracking of the specimen. Figure A6 shows ductile rupture typical of the remaining ligament for comparison.

Fig. A3. Fracture Surface of Finger B₄ Showing Location of Photomicrographs in Figures A4-A6 at A, B, and C Respectively. Direction of crack propagation given by arrows.

Fig. A4. Fracture Surface of Finger B₄, Showing Relatively Small and Indistinct Striations Close to the Initiation Site. (a) 1000 $\times$. (b) 3000 $\times$.

Fig. A5. Fracture Surface of Finger B₄, Showing Striation Size Increases at a Late Stage in the Final Portion of the Test. (a) 1000x. (b) 3000x.

Fig. A6. Fracture Surface of Finger B₄, Showing Ductile Rupture of Remaining Ligament. (a) 1000 $\times$. (b) 3000 $\times$.

In the early portion of fatigue cracking, the striations (Fig. A4) are smaller and less distinct than later in the test (Fig. A5). However, these changes in the striations occurred smoothly with increasing crack length; with no noticeable discontinuities. This indicates that the increase in spacing was due to an increase in stress intensity caused by increasing crack length at constant load limit, rather than by a sudden increase in the load maximum. Hence it was concluded that no appreciable fatigue crack growth occurred until the final portion of loading.

The normal lack of fatigue striations at short crack lengths precludes the determination of the point in the test at which the crack actually initiated. However, the transition from the initiation site to fully developed striations was smooth, giving no obvious indications of a load change in that region. These results are typical of all three fingers.