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**Creep Properties of
Forged 2219 T6 Aluminum
Alloy Shell of General-
Purpose Heat Source-Radioisotope
Thermoelectric Generator**

J. P. Hammond

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METALS AND CERAMICS DIVISION

CREEP PROPERTIES OF FORGED 2219 T6 ALUMINUM ALLOY SHELL OF GENERAL-
PURPOSE HEAT SOURCE-RADIOISOTOPE THERMOELECTRIC GENERATOR

J. P. Hammond

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CREEP PROPERTIES OF FORGED 2219 T6 ALUMINUM ALLOY SHELL OF GENERAL
PURPOSE HEAT SOURCE-RADIOISOTOPE THERMOELECTRIC GENERATOR

J. P. Hammond

ABSTRACT

The shell (2219 T6 aluminum forging) of the General Purpose Heat Source-Radioisotope Thermoelectric Generator (GPHS-RTG) was designed to retain the generator under sufficient elastic stress to secure it during space flight. A major concern was the extent to which the elastic stress would relax by creep. To determine acceptability of the shell construction material, proof tests simulating service were performed. These tests included (1) 600 h of testing at 270°C under 24.1 MPa stress followed by 10,000 h of storage at 177°C under 55.1 MPa, both on the ground and (2) 10,000 h of flight in space at 270°C under 34.4 MPa stress. Additionally, systematic creep testing was performed at 177 and 260°C to establish creep design curves (stress-time relationships for 0.1 and 0.2% total creep and stress-minimum-creep-rate $\dot{\epsilon}_m$ relationships).

The creep tests performed on 2219 T6 at 177°C revealed comparatively large amounts of primary creep followed by small amounts of secondary creep. The early creep is believed to be abetted by unstable substructures that are annealed out during testing at this temperature. The $\dot{\epsilon}_m$ values at this temperature increased from $1.9 \times 10^{-6}\%/h$ at 27.6 MPa stress to $4.0 \times 10^{-6}\%/h$ at 48.3 MPa.

The creep tests performed at 260°C showed normal primary creep followed by large amounts of secondary creep. The $\dot{\epsilon}_m$ values increased from $1.3 \times 10^{-5}\%/h$ at 26.2 MPa to $2.1 \times 10^{-4}\%/h$ at 65.5 MPa.

Duplicate proof tests simulating the ground exposure conditions gave results that were in good agreement. The $\dot{\epsilon}_m$ values at 177°C were 3.25×10^{-6} and $3.75 \times 10^{-6}\%/h$, which amount to an average of less than 0.04% creep in 10,000 h. The primary creep is not of great concern because it can be largely offset by operational and design features.

The proof test that simulates space flight at 270°C gave 0.11% primary creep followed by 0.59% secondary creep. About 10% of the second-stage creep was caused by four or five instantaneous strains, called "strain bursts," which began at the 4500-h mark.

One or two strain bursts, which measured 0.01 to 0.02%, occurred in each of several other tests at 177 and 260°C but were assessed as very moderate in magnitude. The effect is attributable to a slightly microsegregated condition remaining from the original cast structure.

INTRODUCTION

The objective of this program is to provide long-term creep test data for the aluminum outer shell (2219 T6 aluminum forging) of the General Purpose Heat Source-Radioisotope Thermoelectric Generator (GPHS-RTG), an electric power supply for spacecraft. The essential requirement of the shell is to retain the RTG firmly under elastic stress during ground tests and storage in air (condition II) and during subsequent flight in space (condition I).

The aluminum 2219 T6 alloy, as a forging, was selected as the construction material for the shell because of its favorable strength-to-weight ratio. Between 204 and 260°C, 2219 T6 shows the best combination of tensile and creep-rupture strength of all the aluminum alloys.¹ In addition, it has excellent weldability by the gas, arc, or resistance processes. However, the current application involves service in excess of 20,000 h at elevated temperature, and the literature showed that the available creep data on the 2219 alloy scarcely exceeded 1000 h.

A cutaway drawing of the GPHS-RTG is shown in Fig. 1 (ref. 2). The heat source assembly consists of 18 rectangular modules held together by a longitudinal compressive preload applied by spring-loaded frames at each end. The longitudinal preloads are reacted out by the aluminum outer shell assembly. Lateral support at the midspan of the heat source assembly is provided by four radial spring-loaded pins. The preload from these pins is reacted out by the midspan ring of the aluminum outer shell assembly. The support system is shown schematically in Fig. 2 (ref. 2).

Because of thermal expansion of the aluminum shell relative to the heat source and its support system, the longitudinal and radial preloads change during different phases of assembly test, storage, and flight. Should excessive creep occur in the aluminum shell, the spring-loaded heat source support system would relax the preloads and thereby make inadequate the support of the heat source assembly.

During the manufacturing cycle, the RTG outer case will be stress relieved at about 177°C. The case will then receive an emissivity coating, which will be baked at about 260°C for 100 h.

GPHS-RTG

ORNL-DWG 81-12877

Fig. 1. Assembly drawing of General Purpose Heat Source-Radioisotope Thermoelectric Generator. Courtesy of General Electric Company, King of Prussia, Pa.

Fig. 2. Heat source assembly support system.

Following assembly of an RTG, the case will experience maximum temperatures of about 270°C for an estimated 600 h under vacuum. This period includes processing and acceptance testing. The RTG will then be placed in storage with a maximum case temperature of about 177°C for an estimated 10,000 h. The RTG will then be launched, and the case will be subjected to a maximum temperature of 270°C for the duration of the mission. During vacuum operation and storage, the outer shell will be subjected to stresses from application of the internal preload to the heat source assembly.

TEST MATRIX

In collaboration with representatives of the General Electric Company, Valley Forge, Pennsylvania, the test matrix was developed for determining the creep strain in the aluminum shell under service conditions. Analysis of the creep results gave elongations in the shell that were within permitted limits, as shown in Table 1.

Table 1. Predicted creep in Radioisotope Thermoelectric Generator outer shell

Direction	Deformation		Launch preload (%)
	(cm)	(in.)	
Longitudinal	0.079	0.031	13
Radial	0.010	0.004	12

Preload design margin = 25%.

The test matrix is presented in Table 2. The matrix calls for two proof tests to determine creep strain under the most intense stress-temperature conditions envisioned for the shell during the 10,000-h flight in space at 270°C (test I, Table 1) and the combined ground test at 270°C and 10,000-h storage at 177°C (test II). To save time, these tests were performed separately rather than in tandem on the same specimen (test II followed by I). In interpreting results, we invoked the life fraction rule.³

Table 2. Creep test matrix of 2219 T6 alloy forging

Test	Pretest age	Test temperature (°C)	Stress		Time (h)	Orientation ^a
			(MPa)	(ksi)		
I, proof	None	270	34.4	5.0	10,000	LT
II, proof	600 h at 270°C 24.1 MPa (3.5 ksi)	177	55.1	8.0	10,000	LT
III, design data		260	TBD (DC and $\dot{\epsilon}_m$) ^b		10,000	LT
IV, design data		177	TBD (DC and $\dot{\epsilon}_m$) ^b		10,000	LT

^aLT = long transverse.

^bTBD = to be determined; DC = design curves consisting of stress-time relationships for 0.1 and 0.2% total creep; and $\dot{\epsilon}_m$ = minimum creep rate.

This principle states that the amount of creep and the fraction of rupture life used up under one set of service conditions is independent of previous conditions and that the effects of exposure under the separate conditions are cumulative. However, this rule generally is not valid if conditions are severe enough to promote significant microstructural changes in the metal, and results obtained from separate tests should be checked with a single inclusive test (as time permits).

To aid in assessing cumulative plastic strain in the shell, design curves establishing the stress-time relationships for 0.1 and 0.2% creep at 260 and 177°C were determined. Because we expected that the design could preclude a large share of the primary (first stage) creep, the relationships between minimum creep rate and stress at 260 and 177°C were determined to permit computation of strains from secondary creep projected to stresses and times of loading.

MATERIALS AND METHODS

Creep specimens for the tests were machined from the end-cropping of the 2219 T6 forging from which two RTG shells were to be fabricated by the General Electric Company. The forging was 32 cm in diameter and 244 cm long, having been worked down from a 38 × 64 × 116 cm cast ingot. It was purchased from Webber Metals of Paramount, California, under specification QQ-A-367H. The chemical analysis, which meets specification requirements, is

Composition of 2219 Al melt No. K-60-303, wt %

Si	Fe	Cu	Mn	Mg	Zn	Ti	Zr
0.08	0.15	6.09	0.32	0.01	0.02	0.06	0.13
				Others	Total others		
				0.05 max	0.15 max		

After drilling a 20.3-cm-diam hole through the axis of the forging, the following conventional heat treatment was performed.

Solution anneal: 5 h at 535°C

Quench: in 18% glycol solution at 23°C with 5- to 8-s
delay time

Artificial age: 26 h at 191°C

Because the forging had such large dimensions and therefore contained less mechanical fibering than usual, the purchaser placed less stringent strength requirements on the material in the longitudinal direction than required by specification QQ-A-367H. The room-temperature tensile properties measured on the forging in the three principal directions are compared with the specification and purchaser's requirements in Table 3. Although measured properties meet the purchaser's requirements, they fall significantly below those of the specification in the longitudinal direction.

Blanks were taken from the central region of the cropping of the drilled forging, both in the long-transverse (tangential) and longitudinal directions, and creep specimens with a 0.63-cm-diam by 3.2-cm-length gage were machined in accordance with the requirements of the specimen drawing in Fig. 3.

Creep testing was performed in air with standard lever-arm machines having lever ratios of 6/1 to 12/1. The test methods used exceeded the requirements⁴ of ASTM E 139-70. The creep machines were calibrated to a load accuracy of $\pm 0.5\%$. Temperature was measured by three Chromel-Alumel thermocouples ($\pm 3/8\%$ accuracy) wired to the gage section. The temperature variation of the gage was less than $\pm 1.0^\circ\text{C}$, and the highest temperature (central) was taken as the nominal test temperature. Proportioning temperature controllers were used to maintain the temperatures of the furnaces to $\pm 1.0^\circ\text{C}$.

Creep measurements were made with a double (large) dial gage extensometer (Fig. 4). This extensometer averages strain between two measurements and corrects for errors from nominal specimen bending. Strain is measured to an accuracy of $\pm 2.5 \mu\text{m}$ (0.0001 in.).

Table 3. Room-temperature tensile properties of
2219 T6 aluminum forging

	Ultimate tensile strength		Yield strength		Elongation (%)
	(MPa)	(ksi)	(MPa)	(ksi)	
Longitudinal					
Measured	378	54.8	259	37.5	11.0
Required by purchaser	372	54.0	248	36.0	
Required by specification QQ-A-367H	400	58.0	276	40.0	
Tangential					
Measured	383	55.5	262	38.0	6.4
Required by purchaser	379	55.0	255	37.0	
Required by specification QQ-A-367H	379	55.0	255	37.0	
Radial					
Measured	363	52.7	241	35.0	
Required by purchaser	359	52.0	241	35.0	
Required by specification QQ-A-367H	365	53.0	241	35.0

Fig. 3. Creep test specimen.

Fig. 4. Dual-dial gage extensometer.

EXPERIMENTAL RESULTS

Results of the creep tests performed on the 2219 T6 forging are summarized in Table 4. Total creep strains ϵ_p for time in test t are given along with the constant rate of secondary creep $\dot{\epsilon}_m$. Several of the tests either failed or were discontinued to make creep frames available for additional tests. The individual computer readout creep curves with incidental computations for these tests are presented in the Appendix.

Table 4. Results of creep test^a on 2219 T6 alloy forging

Test	Specimen ^b	Temperature		Stress		Creep $\dot{\epsilon}_p$ (%)	Time t (h)	Rate $\dot{\epsilon}_m$ (%/h)	Status
		(°C)	(°F)	(MPa)	(ksi)				
I, proof	LT-11	270	518	34.4	5.0	0.681	9276	4.5×10^{-5}	Running
II, proof A	LT-6	270	518	24.1	3.5	0.084	597	^c	Running
	LT-6	177	350	55.1	8.0	0.057	9237	3.25×10^{-6}	
II, proof B	LT-16	270	518	24.1	3.5	0.063	645	^c	Running
	LT-16	170	350	55.1	8.0	0.060	3788	3.75×10^{-6}	
III, design	LT-7	260	500	65.5	9.5	0.384	979	2.15×10^{-4}	Discontinued
III, design	LT-12	260	500	56.5	8.2	0.497	2600	1.04×10^{-4}	Failed
III, design	LT-17	260	500	53.1	7.7	0.870	4065	1.44×10^{-4}	Failed
III, design	LT-9	260	500	41.4	6.0	0.442	8454	3.7×10^{-5}	Running
III, design	LT-14	260	500	34.5	5.0	0.116	4799	2.18×10^{-5}	Discontinued
III, design	T-1	260	500	26.2	3.8	0.213	5652	1.3×10^{-5}	Running
IV, design	LT-8	177	350	48.3	7.0	0.243	8586	4.0×10^{-6}	Running
IV, design	LT-13	177	350	40.0	5.8	0.135	7614	4.0×10^{-6}	Running
IV, design	LT-18	177	350	31.7	4.6	0.105	4372	2.0×10^{-6}	Discontinued
IV, design	T-2	177	350	27.6	4.0	0.226	4410	1.9×10^{-6}	Running
Preliminary	L-1	260	500	65.5	9.5	0.154	1026	4.6×10^{-5}	Discontinued
Preliminary	L-2	177	350	55.1	8.0	0.221	1145	1.5×10^{-5}	Discontinued

^aConducted in air.^bLT = long transverse orientation; L = longitudinal.^cIn primary creep stage.

The creep curve for proof test I is shown in Fig. 5, and Figs. 6 and 7 give the curves for duplicate runs of proof test II. The stages of creep response are marked on the graphs along with computations of ϵ_m . Proof test I (10,000-h space flight at 270°C and 34.4 MPa stress) shows a primary stage of creep from A to B and secondary creep from B to C, for which the constant rate of creep is $4.5 \times 10^{-5}\%/h$. At this rate of creep, the total strain from secondary creep after 10,000 h would be 0.45%, and, when added to the primary creep, the total creep would be 0.56%.

However, after about 4500 h, the curve shows three small strain offsets (at C, D, and E) before exhibiting a segment of somewhat increased slope (EF). Still a fourth distinct offset is seen at F, seemingly followed by another straight segment (FG) of slope slightly increased over that of EF. The test has gone 9276 h and given a total creep strain of 0.68%. With the current rate of creep and barring additional strain offsets, the creep after 10,000 h would be about 0.76%. This is to be compared with the strain of 0.56% had the test continued at the initial constant creep rate and not incurred the unpredictable strain offsets and inflections upward.

Fig. 5. Creep curve for proof test I (space flight).

The observed offsets occurred in several of the other creep tests both at 260 and 177°C, and, because they possibly could contribute significantly to the total creep relaxation of stress in the RTG shell, they will be discussed in more detail subsequently. The phenomenon is not a common one and is referred to in the literature as creep bursts^{5,6} or jumps.⁷ Such strain bursts are usually associated with weld⁵ or cast⁶ structures.

The first of the two proof tests II (600-h pretest age at 270°C and 24.1 MPa followed by 10,000-h ground storage at 177°C and 55.1 MPa) logged in 9237 h of storage (Table 2), and results of the test now appear to be predictable. The projected creep strain for the completed test is 0.136% (Table 2). Region A-B in Fig. 6 represents the creep response during on-ground aging, B-C shows the strain sustained from taking the tests from aging to storage, and C-D-E gives the creep response during the first 9237 h of storage. The strain from A to D in Fig. 6 is essentially primary creep, and D-E is steady state, for which $\dot{\epsilon}_m$ is only $3.25 \times 10^{-6}\%/h$. It is significant that the earlier aging (A-B) dissipates the bulk of the primary creep and leaves only about 0.025% (C-D) to be dissipated during storage. Also, the minimum creep rate during storage (DE in Fig. 6) is very low, giving a projected secondary creep of only 0.033% in 10,000 h.

The duplicate of proof test II gave a curve (Fig. 7) closely conforming to the earlier one. Although this test went only 3788 h into storage, its secondary creep rate ($3.75 \times 10^{-6}\%/h$) is very close to that of test IIA. The transition strain B-C was 0.58% compared with 0.59% for the earlier test. Thus, barring effects from any future strain bursts in test IIB, the test appears to be reproducible.

Design curves of the relationships of stress and time to 0.1 and 0.2% total creep at 260°C (test III of Table 2) are presented in Fig. 8. The individual creep graphs on which the design curves are based are given in the Appendix, tests LT-7 through T-1. The design curves suggest that, for 10,000 h, a stress of about 29.0 MPa would be required to produce 0.1% creep strain and that a stress of 35.8 MPa would be needed to produce 0.2% creep. However, the LT-14 data point for 0.2% strain is based on an extrapolation from data to about 5000 h; in view of the strain bursts and inflections cited earlier for proof test II (270°C), the validity of the

Fig. 8. Design curves of 2219 T6 forging showing stress-time relationship for 0.1 and 0.2% creep at 260°C.

right portion of the 0.2% graph perhaps should be held in abeyance until longer time is accrued for LT-14. So far, the only evidence for possible strain bursts in the 260°C tests appeared in the LT-9 and T-1 tests at times of about 7500 and 5000 h, respectively (see Appendix).

In Fig. 9 data showing the relationship between stress and minimum creep rate are plotted for the tests at 260°C. The data point obtained from proof test I at 270°C (LT-11) is included. Because such data plotted on a log-log graph frequently give a straight line, they are so represented in this figure. The data show very little scatter. By extrapolation to lower stresses, these results suggest that, at 260°C, a stress of 31.0 MPa would be required to produce 0.2% strain by secondary creep and a stress of 24.8 MPa to give 0.1% strain.

The stress-time relationship to produce 0.1% total creep and the relationship between stress and minimum creep rate for the tests performed at 177°C are presented in Figs. 10 and 11, respectively. The data for creep tests performed at 260°C are included for comparison. Incidents of strain burst at 177°C occurred for test LT-13 and T-1 (see Appendix).

As a result of the extremely low minimum creep rates for the tests conducted at 177°C (1.9 to $4.0 \times 10^{-6}\%/h$), the creep curves usually failed to extrapolate to total strains of 0.2% within 10,000 h. As a result, the

0.2% strain design curve is not given for the 177°C temperature. Also, because the amount of primary creep at 177°C appeared to be somewhat greater and to occur at a faster rate than at 260°C, the design curve for 0.1% strain at 177°C fell in a region of lower stress than did the 0.1% curve at 260°C. This (illustrated in Fig. 10) was most unexpected.

Fig. 9. Graph of 2219 T6 forging showing stress-minimum creep rate relationship at 260°C.

Fig. 10. Design curves of 2219 T6 forging comparing stress-time relationships for 0.1% creep at 177 and 260°C.

Fig. 11. Graph of 2219 T6 forging comparing stress-minimum creep rate relationships at 177 and 260°C.

On the other hand, the relative behavior of minimum creep rate between the two test temperatures was expected; that is, the effect of decreasing the test temperatures was to decrease the minimum creep rate. This is shown in the graph in Fig. 11, which presents the stress-minimum creep rate curves at 177 and 260°C.

At 177°C a projected stress of only about 20.7 MPa was required to produce 0.1% creep in 10,000 h, whereas at 260°C a stress of 28.9 MPa was required, as pointed out earlier (Fig. 10). For 0.1% strain by secondary creep, it appears on the basis of the data ($\dot{\epsilon}_m$ mark of $10^{-5}\%/h$ in Fig. 11) that about 62.0 MPa would be required at 177°C, whereas (as noted earlier) a stress of only 24.8 MPa would be needed at 260°C.

The preliminary tests listed in Table 2 (L-1 and L-2) were for specimens taken in the longitudinal direction of the forging and tested for comparatively short times (~1000 h) at the two test temperatures. All other specimens tested were of long-transverse orientation.

A comparison of the secondary creep rates on the longitudinal specimens at 260 and 177°C with creep rates established at the same stresses and over the same time for the long-transverse specimens indicated the latter to have the lower creep strength.

At 260°C the long-transverse specimen crept more than 4 times faster than did the longitudinal, and at 177°C more than 3 times faster. This was expected. The room-temperature tensile and yield strengths of the 2219 T6 aluminum alloy as a die forging, for example, are reported to be somewhat higher in the direction parallel to the grain axis than at right angles.⁸

DISCUSSION

The most pertinent information on the creep-rupture properties of the 2219 T6 aluminum forge alloy with which to compare our results was developed by the Alcoa New Kensington Laboratory in the sixties.⁸ This data included time to 0.1 and 0.2% strain from room temperature to 371°C at 56°C increments but excluded the 204°C test temperature and did not go beyond 1000 h. Our 0.1 and 0.2% creep design curves at 260°C are compared with the Alcoa data in Fig. 12.

The Alcoa data reflect considerably higher resistance to creep in the region around 1000 h and higher. However, the two separate sets of curves converge on one another as the time of testing diminishes, suggesting that the tensile strengths of the materials may not be greatly different.

Because the Alcoa data fail to report the orientation of test specimens in the forging, it is possible that the moderate superiority of the Alcoa data over ours may result from their test specimens having been taken from the longitudinal orientation. A second possible explanation may lie in the comparative grain sizes of the two forgings and differences in chemical homogeneity resulting from the extent of hot working.

Figure 13 shows the microstructure of the failed creep specimen LT-12 (long-transverse orientation) after testing at 260°C. The field of view is in the gage length normal to the specimen axis. Note that the grain size is very large (>ASTM No. 1) and that considerable coring (micro-segregation) from the cast billet remains in the forging. The trace amount of imperfections (arrows) is typical and is believed to be shrinkage cavities not closed by the forging operation.

Figure 14 shows the microstructure of specimen LT-12 at the failed surface (field of view parallel to specimen axis). An internal semi-continuous rupture is seen off the failed interface, and both fracture

Fig. 12. Comparison of our design curves at 260°C (stress-time for 0.1 and 0.2% creep) of 2219 T6 forging with Alcoa data.

surfaces clearly are intergranular. Because the test temperature (260°C) was approximately 0.6 of the alloy's absolute melting temperature (high in relation to equicohesive temperature), it is not surprising that the specimen failed intergranularly instead of transgranularly.

The creep bursts that we observed in proof test I (270°C), test LT-9 (260°C), and LT-13 (177°C) produced small amounts of strain compared with that of other investigators where the phenomenon has surfaced.⁵⁻⁷ Sikka and David obtained bursts as large as 1.7% on cast ingots of type 316 stainless steel, whereas ours ranged only from 0.01% to about 0.02% in size. They generally obtained zero to three bursts per test, whereas we obtained zero to two bursts per test except for proof test I (Fig. 5), where four or possibly five bursts occurred. The strain from bursts in the latter test gave an accumulated strain of 0.07%, or about 10% of the total strain. In assessing the strain from bursts in our tests, one must recognize that some of the creep tests were not run to completion.

Sikka and David⁶ associated the creep burst phenomenon with cast or welded structures and attributed it to the differential concentration of creep "strengthening elements" in the interdendritic regions as compared with the dendrite arms. They suggest that the prominence of bursts is related to the coarseness and degree of segregation in the cored

structure. Although Da Silveria and Monteiro⁷ report creep bursts in wrought wires of types 316 and 304 stainless steel, Sikka and David point out that the open literature has examples of researchers' reporting their material to be wrought but that the material still contained as-cast inhomogeneities. Our instance of the observed slightly cored microstructure in the current 2219 T6 forged material appears to support this contention.

Fig. 13. Microstructure of failed creep specimen LT-12 removed from fracture surface.

The most unexpected material behavior in this study is illustrated by Fig. 10 and is associated with the fact that the 0.1% creep design curve at 177°C falls into a stress regime substantially lower than that for the 260°C design curve. This behavior means that, for the time frame of 100 h, for example, a lower stress is required to produce 0.1% creep

Fig. 14. Microstructure of failed creep specimen LT-12 at fracture surface.

strain at 177°C than at 260°C. Ordinarily, a higher stress would be required. A second manifestation of this behavior is that, for the same loading stress, a shorter time is required to produce 0.1% creep at 177°C than at 260°C. Again, the converse of this was expected; that is, a longer time should be required to produce 0.1% creep at 177°C than at 260°C. The foregoing judgments, of course, are based on the premise that creep is caused by metal flow and that the activation energy is lower for such flow at the higher temperature.

It is possible that the foregoing behavior results from an unstable microstructural state that contributes to creep at the 177°C test temperature but that does not do so at 260°C because of its having been annealed out during the loadup period incident to testing.

The foregoing explanation may be shown to be plausible by examining creep behavior deduced from stress-rupture tests performed on 2219 T6 forged material at the Alcoa New Kensington Laboratory.⁸

Figure 15 presents creep-rupture data with stress and temperature as the coordinates. Curves are shown for 0.1, 0.2, and 0.5% creep and rupture. Note that a consistent pattern is portrayed at all test temperatures except in that temperature region having thermal recovery of the material as a factor ($\sim 191^{\circ}\text{C}$). Observe also that the recovery affects only the early stage of creep and does not appear to influence later stages (secondary and tertiary creep). Finally, microstructural instability responsible for the anomaly does not seem to operate at the lower temperatures; that is, the unstable structure must not anneal out at 100°C and lower.

Fig. 15. Stress-temperature relationship for increasing amounts of creep of 2219 T6 forging.

RECOMMENDATIONS

Two recommendations are made to afford a better understanding of the projected service performance of the 2219 T6 aluminum shell component of the GPHS-RTG. First, it is advisable after the earlier tests are completed to subject the two tests now under way to the conditions of space flight (proof test I) in order to assess the creep that occurs with prestorage and ground storage (proof tests IIA and B). This is needed to examine the implications of the life fraction rule as applied to the service performance of this component.

One aspect of this rule is that exposure effects at different conditions are cumulative. For example, if one-third of the material's service life (or time to initiation of tertiary creep) is used up at 177°C, only two-thirds of its life should remain for service at 270°C. The life fraction rule generally works better when the earlier service is at the lower temperature rather than the higher temperature,³ as is the case for our ground storage and space flight.

Things to be established by the consecutive proof tests on the same sample include (1) the extent to which the primary creep in the space exposure (test I) is diminished as a result of the prior history on the ground (test II); (2) the extent, if any, to which secondary creep rate in space is affected by prior exposures; and (3) the extent to which the useful life in space is diminished (time of second-stage creep) by prior exposures.

The second recommendation is that the creep specimens exposed at 177 and 260°C be examined by electron transmission metallography to explain why the creep behavior during early-stage creep appears to be distinctly different at 177°C from that at the higher temperature. It is possible that the excessive primary creep activity at 177°C is caused by a polygonization of prevailing substructures or movement of dislocation entanglements at this temperature.

CONCLUSIONS

Investigation of the creep properties of the 2219 T6 aluminum alloy forging to be used for the outer shell component of the GPHS-RTG led to the following conclusions:

1. Duplicate proof tests to assess the magnitude of creep during prelaunch testing and storage (600 and 10,000 h, respectively) on the ground gave results in excellent agreement. Although primary creep was considerable (0.11 and 0.12%), this strain can be largely eliminated by tightening the shell supports after testing. The rate of second-stage creep was extremely low, averaging $3.5 \times 10^{-6}\%/h$ and amounting to a strain of only 0.035% during ground service.

2. The proof test simulating conditions in space (10,000 h) gave about the same primary creep (0.11%). However, the major portion of this strain is not likely to occur because of the prior creep history on the ground. The minimum creep rate of the second-stage creep was $4.5 \times 10^{-5}\%/h$ or 0.45% in 10,000 h. Mild "creep bursts" occurred during the latter half of the test, which increased the second-stage creep to about 0.66%.

3. We concluded that the major element of concern about the relaxation of stress in the shell by creep centers on the magnitude of second-stage creep during the service in space at 270°C, 34.4 MPa. Although our assessments of creep for ground and space conditions were obtained by separate simultaneous tests (to save time), the life-fraction rule states that the creep life during later exposure is diminished in direct relationship to the fraction of life used up in early exposure. Therefore, a single continuing test that incorporates the separate service conditions in tandem is needed to assess validly the strain and/or life during service.

ACKNOWLEDGMENTS

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APPENDIX

Computer Graphs of Creep Tests

2/2

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Table A1. Computer graphs of creep tests

Test	Specimen	Test time (h)	Strain scale ^a (%/2.54 cm)	Page
21925	LT-11	9276	0.200	28
21820	LT-6	8467	0.050	29
21820	LT-6	9833	0.050	30
22270	LT-16	4433	0.025	31
21838	LT-7	979	0.100	32
21839	LT-12	2600	0.100	33
21867	LT-17	4065	0.100	34
21867	LT-17	1145	0.050	35
21941	LT-9	4712	0.100	36
21941	LT-9	8455	0.200	37
22032	LT-14	4799	0.020	38
22102	T-1	5652	0.050	39
21940	LT-8	7220	0.100	40
21940	LT-8	8586	0.050	41
21968	LT-13	6261	0.100	42
21968	LT-13	7615	0.050	43
22070	LT-18	4372	0.100	44
22250	T-2	4410	0.050	45
21754	L-1	1026	0.050	46
21770	L-2	1145	0.100	47

^aScale reduced 2 to 1 in reproduction.

ORNL-DWG 81-12944

ORNL-DWG 81-12945

21820 STRESS 3500 PSI: TEMP 518 F: AL
 SPEC NO LT-6 L.S. = 0.004 %

② 7/11/74 7/11/74

ORNL-DWG 81-12946

7/1 ③

ORNL-DWG 81-12947

ORNL-DWG 81-12949

21867 STRESS 7700 PSI; TEMP 500 F; 2219 AL
SPEC NO LT-17 L.S. = 0.061

ORNL-DWG 81-12951

8/11/20

7/1

21941 STRESS 6000 PSI TEMP 500 F AL. 1/21/51 7/1 (9)

SPEC NO LT-9 L.S. = 0.078

RUPTURE LIFE 4710.8
RUPTURE ELONGATION 0.0

$$\epsilon_{\text{BC}} = 0.300 + (0.100 \times \frac{7}{20}) = 0.3450$$
$$= 0.100 + (0.100 \times \frac{3}{20}) = 0.1650$$
$$+ 0.180\%$$
$$\epsilon_{\text{m}} = 0.14\% \text{ in } 5,000 \text{ h} = 2.7 \times 10^{-5} \%/\text{h}$$
$$= 2.7 \times 10^{-7} \text{ in/in/h}$$

ELONGATION (PCT)

TIME IN HOURS

0.900
0.800
0.700
0.600
0.500
0.400
0.300
0.200
0.100
0

0 50 100 150 200 250 300 350 400 450 500

$\times 10^3$

0.276
0.125
0.075
0.050
0.025
0.0125
0.00625
0.003125
0.0015625
0.00078125
0.000390625
0.0001953125
0.00009765625
0.000048828125
0.0000244140625
0.00001220703125
0.000006103515625
0.0000030517578125
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ORNL-DWG 81-12953

21941 S15tSS 6000 PSI TEMP 500 F AL.
SPEC NO LT-9 L.S. = 0.078

7/1 (10)

ORNL-DWG 81-12954

ORNL-DWG 81-12955

21940 STRESS 7000 PSI TEMP 350 F AL.
SPEC NO LT-8 L.S. = 0.074 cal.

ORNL-DWG 81-12956

7/1 5/11/81 (13)

ORNL-DWG 81-12957

21940 5 RESS 100 PSI TEMP 350 F AL
 SFE NO LT-8 L 5 = 0.074 Calc

7/1

(14)

ORNL-DWG 81-12958

21968 STRESS 58000 PSI TEMP 350 F AL
 SPEC NO LT-13 L.S. = 0.061 Calc

① 6/10/71 ⑤

ORNL-DWG 81-12959

21968 STRESS 58000 PSI TEMP 350 F AL
SPEC NO LT-13 L S = 0.061 calc

7/1 76

ORNL-DWG 81-12960

ORNL-DWG 81-12961

22250 STRESS 4000 PSI: TEMP 350 F: AL
SPEC NO T-2 L.S. = 0.072

7/1/81 (18)

ORNL-DWG 81-12962

ORNL-DWG 81-12962

21770 STRESS 2975 PSI; TEMP 350 F; AL.
SPEC NO L-2 L.S. = 0120

20

42

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