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Summary

Advanced, subscale, tubular combustion chambers have been built and test fired with hydrogen and oxygen propellants to assess the increase in fatigue life that can be obtained with this type of construction. Two chambers were tested: One ran for 637 cycles without failing, over 400 cycles more than the predicted life (200 cycles) for a comparable smooth-wall milled-channel liner configuration. The other chamber failed at 256 cycles, still beyond the predicted life (118 cycles) for a comparable smooth-wall milled-channel liner configuration. Posttest metallographic analysis determined that the strain-relieving design (structural compliance) of the tubular configuration was the cause of this increase in life.

Introduction

New, advanced combustion chamber configurations have been proposed to replace the conventional milled-channel liner configuration typified by the Space Shuttle main engine (SSME). Potential benefits of the new configurations are improved cyclic life, reusability, reliability, and performance. Performance improvements are anticipated because of the enhanced heat transfer into the coolant, which will enable higher chamber pressures in expander cycle engines. Cyclic life, reusability, and reliability improvements are anticipated because of the enhanced structural compliance inherent in the construction. Structural compliance provides the configuration with the ability to expand without restraint in a circumferential direction during the thermal cycle of a hot firing. This expansion, when restrained as in a conventional milled-channel configuration, results in plastic yielding of the combustor material. The structural compliance is accomplished by providing expansion joints in an axial direction, between each coolant passage.

The method of construction involves forming the combustion chamber with a bundle of high conductivity copper or copper-alloy tubes and bonding these tubes by electroforming. The advantage of the electroform bonding over the more conventional methods of attaching the tubes, such as welding or brazing, lies in the absence of high temperature in the joining. Electroform bonding occurs at room temperature and, as a result, does not degrade the material properties. Detailed descriptions of the fabrication of these configurations are found in references 1 and 2. An analysis of the predicted heat-

transfer performance and the improved fatigue life advantages of this concept are presented in references 3 and 4. In order to evaluate the merits of this concept, a program was undertaken at NASA Lewis to design, fabricate, and test these advanced chamber configurations.

Apparatus

A broad base of fatigue data on copper-alloy milled-channel combustion chambers has been obtained on a NASA Lewis low-cost, subscale rocket-engine test apparatus. The apparatus is used to study the fatigue life of rocket chamber walls, to screen candidate chamber-wall materials, and to evaluate fabrication techniques for improved life and performance. The test apparatus (fig. 1) consists of an injector, a 15.24-cm (6.0-in.) long liquid-hydrogen-cooled outer cylinder, which serves as the test chamber, and a water-cooled centerbody, which forms the combustion, sonic throat, and expansion sections of an annular rocket engine. It is at the sonic throat station that the maximum heat flux is encountered, and this is the location that is studied for fatigue. The standard configuration of the test chamber has 72 rectangular milled cooling passages.

A variation of this standard configuration was used for the work reported herein. A test chamber was designed with a stacked array of oxygen free, high conductivity (OFHC) copper tubes as the coolant passages, as shown in figure 2. To allow more direct comparisons, 72 tubes were used to equal the 72 milled channels of the standard configuration. To provide a coolant-flow area equivalent to the milled-channel liner configuration, and to maintain the same wall thickness, something other than round tubes were required. To meet this requirement, round tubes larger than $1/72$ of the available chamber circumference, were flattened on two sides (booked) to achieve the width of $1/72$ of the available circumference. The tubes were sized to provide the equivalent coolant-flow area when booked. The tube wall thickness was selected to be the same as the milled-channel liner wall thickness, 0.089 cm (0.035 in.), again, for comparison purposes, even though the higher structural efficiency of a circular coolant passage would allow the tube wall thickness to be thinner than an equivalent rectangular-milled coolant passage. The resultant tube design is shown in figure 3.

To provide an effective coolant entrance and exit from the manifolds, right angle bends, with a 0.318-cm (0.125-in.)

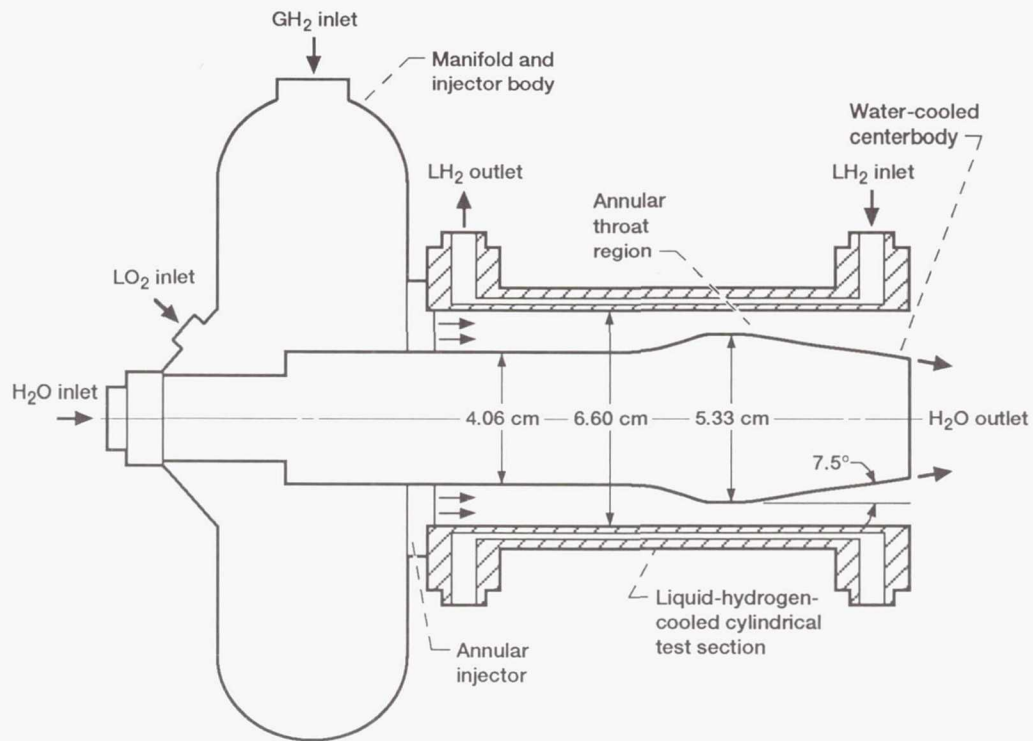


Figure 1.—Subscale rocket engine test apparatus.

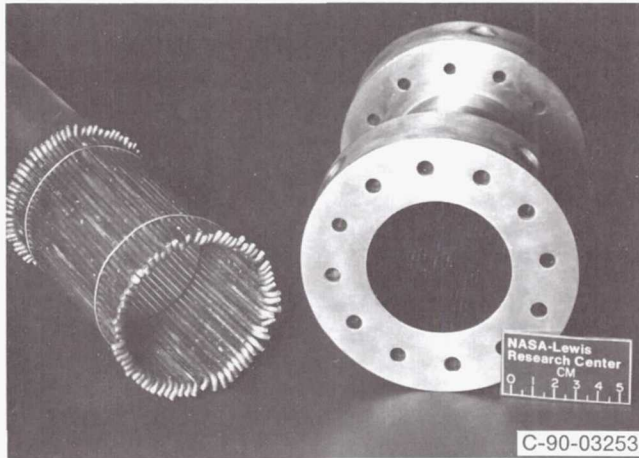


Figure 2.—Stack of OFHC copper tubes and completed sub-scale thrust chamber.

inside radius were made at each end of the tube. These tubes were then stacked around a 6.60-cm (2.6-in.) diameter mandrel and bonded together by a layer of electroformed copper deposited onto the outside diameter of the assembly. During the electroform bonding of the tubes, the sides of the tubes were prevented from bonding. This caused a natural expansion joint that was intended to open and close during cyclic hot firing and, thus, provide structural compliance to the assembly. There were some concerns that the expansion joint would provide stress concentration at the end of the joint and encourage flaw continuation in the form of radial crack growth. No special design features were involved to avoid

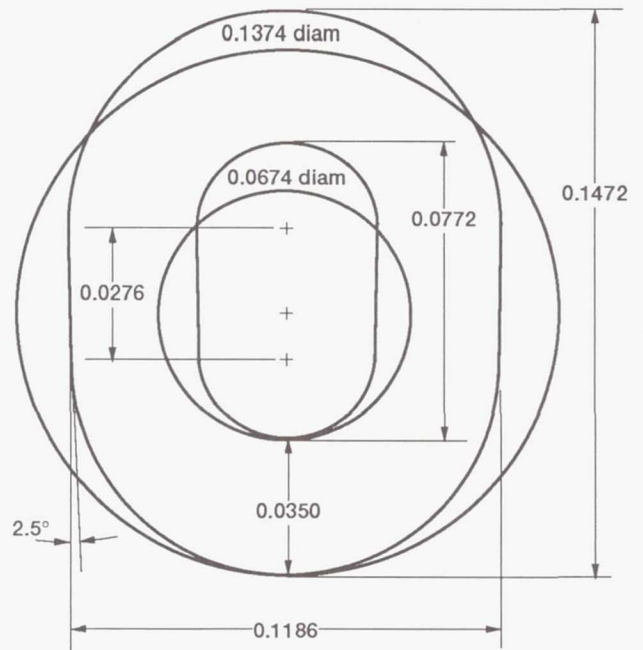


Figure 3.—Diagram of tube dimensions (round to booked). All dimensions in inches.

this, other than careful attention to providing quality copper deposition during electroforming. The right side of figure 2 shows a typical tubular chamber after fabrication. Three chambers of this configuration were built and passed pressure proof tests and leak tests.

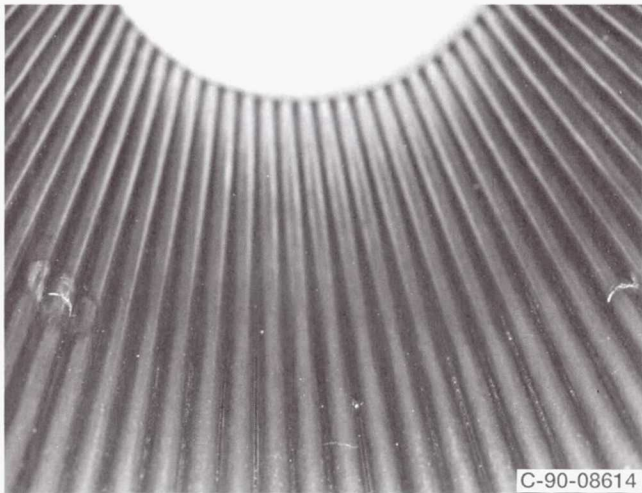


Figure 4.—Inside view of tubular chamber showing two installed crown thermocouples.

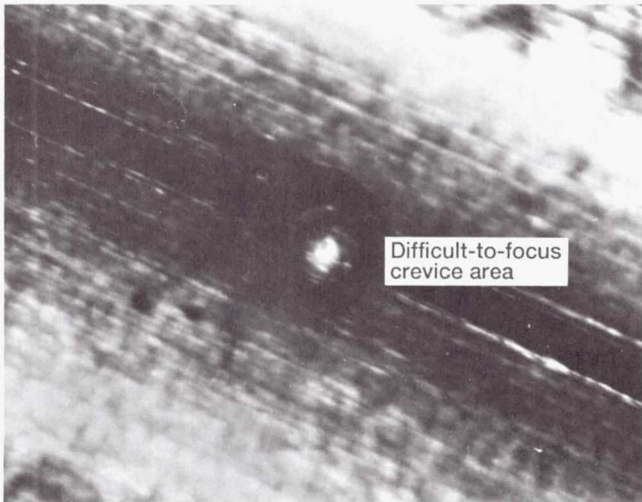


Figure 5.—Thermocouple junction in the crevice area.

Instrumentation

Of the three chambers available, one had special intrusive instrumentation, while the other two did not. The intrusive instrumentation involved the insertion of thermocouples onto the combustion-side surface of the tubes at the throat station. This chamber had two types of thermocouples on the combustion-side surface. The first was a chromel/alumel thermocouple in a 0.0254-cm (0.010-in.) diameter sheath placed in a 0.03-cm (0.012-in.) deep groove cut across the crown of a coolant tube on the combustion-side surface. Figure 4 shows the installation of the crown thermocouples. The second was also a chromel/alumel thermocouple in a 0.0508-cm (0.020-in.) sheath, which was placed between two tubes in the crevice region, flush with the combustion-side surface. Figure 5 shows the thermocouple junction in the crevice. These thermocouples were installed in four circum-

ferential locations, 90° apart, at the throat plane. The four crevice thermocouples were located 45° from the four crown thermocouples. In addition, eight chromel/constantan thermocouples were attached to the outside diameter surface, four at the throat and four 1.27 cm (0.50 in.) downstream of the throat.

The drilling and grooving necessary to install these thermocouples significantly weakened the structure of this chamber. Because of this, the structural integrity of this chamber was seriously degraded, and it was used only to establish hot-gas-side wall temperatures as a function of liquid-hydrogen-coolant mass-flow rate. No life tests were performed on this chamber.

The other two chambers, however, had only nonintrusive thermocouples installed on the outside diameter surface of the combustion chamber. These chromel/constantan thermocouples were located at four circumferential locations, 90° apart, at the throat plane.

The liquid-hydrogen coolant-inlet temperature was measured by a platinum resistance bridge transducer inserted into the inlet manifold. The hydrogen outlet temperature was measured by a chromel/constantan thermocouple inserted into the outlet manifold. The combustion-chamber conditions, as described in references 5 and 6, were monitored for combustion-chamber pressure, propellant weight flows, and coolant weight flow.

Procedure

The test procedure for these advanced tube-bundle chambers was identical to that used for the fatigue tests described in references 5 and 6. These tests used gaseous hydrogen and liquid oxygen as propellants at a nominal mixture ratio of 6.0 and were conducted at a combustion-chamber pressure of 4137 kN/m² (600 psia). The wall temperature was controlled by a separate mass flow of liquid hydrogen through the cooling passages. The chamber was fired for 1.7 sec to allow the chamber to reach thermal equilibrium and was then shut down for 1.8 sec while the coolant continued to flow to get to steady state cold conditions. The total cycle time was 3.5 sec. This 3.5-sec cycle was repeated approximately 50 times. The combustion chamber was then inspected and subjected to another test series. This was repeated until a sufficient number of cycles were accumulated or until a combustion-chamber failure was detected by sensing a coolant-passage leak.

The first chamber to be tested was the one that was intrusively instrumented to establish the hot-gas-side wall temperatures as a function of liquid-hydrogen-coolant mass-flow rate. This chamber was fired for a total of eight cycles, two cycles at each of four different conditions. The two cycles were run to calibrate the propellant valves and adjust the combustion conditions. The second two were run at nominal combustion conditions ($P_c = 4137 \text{ kN/m}^2$ (600 psia), $O/F = 6.0$) and a facility maximum coolant flow of 0.841 kg/sec (1.98 lb/sec).

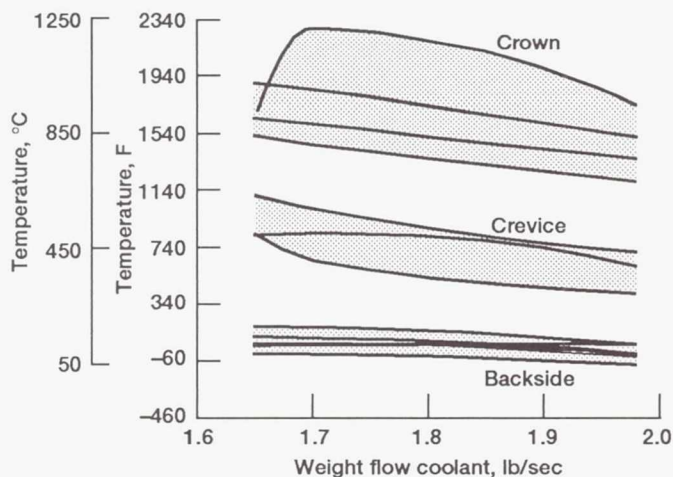


Figure 6.—Instrumented tube chamber data.

The third set of two cycles was run at a coolant flow of 0.777 kg/sec (1.70 lb/sec). The last two were run at a coolant flow of 0.759 kg/sec (1.65 lb/sec).

The second chamber was test fired at the nominal combustion conditions using a coolant flow of the facility maximum which was 0.841 kg/sec (1.98 lb/sec). As a result of the tests on the first chamber, these conditions were expected to cause the crowns of the tubes at the throat to operate at temperatures up to 570 °C (1060 °F). The third chamber was also test fired at nominal combustion conditions, but the coolant flow was reduced to 0.807 kg/sec (1.78 lb/sec). These conditions were expected to cause the crowns of the tubes in the throat to operate at temperatures up to 671 °C (1240 °F).

Results and Discussion

The first hot-fire tests were performed to determine flow set points and to measure the hot-gas-side wall temperatures. The results of these tests are shown in figure 6, which is a plot of the various temperature measurements as a function of coolant flow rate. The melting-point temperature of the OFHC copper tubes is 1083 °C (1981 °F). Clearly, some of the crown thermocouples were not correct. The measurement of crown temperatures by milling away almost one-half of the wall thickness, to provide a groove for the thermocouple and then replacing the copper of that groove with stainless steel, ceramic, and chromel/alumel wires, may have unduly affected the true crown temperatures. The crevice temperature thermocouples, however, are located in a more forgiving environment and are more likely to provide some insight into the severity of the thermal environment.

A SINDA two-dimensional, conduction heat-transfer analysis was performed to evaluate the sensitivity of the difference between the crown and crevice temperature as a function of coolant mass-flow rate. This study predicted a 188.6 °C (340 °F) temperature difference from the crown to

the crevice for the configuration tested and analyzed. This value has been shown by posttest analysis to be somewhat less than 222 °C (400 °F). By applying this increment to the measured crevice temperatures at the maximum coolant flow condition of 0.841 kg/sec (1.98 lb/sec), we can predict a range of crown temperatures with a maximum of 570 °C (1060 °F). This was the condition that was selected for the fatigue test of the second chamber. The second chamber was test fired for 637 cycles with no sign of fatigue cracks. Since this chamber exceeded the life of an equivalent milled-channel configuration by over a factor of three, testing was stopped. Reference 5 predicts a life of 200 cycles for the 72 milled-channel configuration if tested at a wall temperature of 570 °C (1060 °F).

Figure 7 shows the inside surface of the second chamber after the 637 firing cycles. Surface roughness measurements were made on the tube centerlines at the throat prior to testing and, again, after 637 cycles. The roughness before testing was measured as 32 μin., or less. After testing, the average surface roughness in the throat region was 300 μin., with a range from 152 to 534 μin. This roughening, termed "blanching," is caused by cyclically repeated oxidation and reduction reactions between the combustion gases and the copper wall. A thorough examination of this process is reported in reference 7, which includes data of surface roughness as a function of the hot-gas-side wall temperature. For the wall roughness measured on the second chamber (300 μin.), reference 7 predicts a temperature of 830 °C (1525 °F) for the hot-gas-side wall temperature. Although the crown temperature at throat plane at the start of testing was estimated to be 570 °C (1060 °F), the surface deterioration caused by blanching raised the temperature to an estimated 830 °C (1525 °F) before the testing was suspended. Continued cyclic testing would have lead to a chamber failure. Furthermore, the failure would not be a conventional fatigue failure caused by

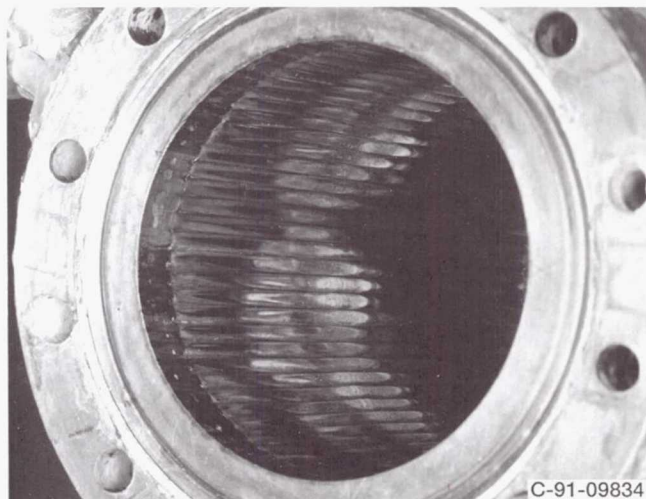
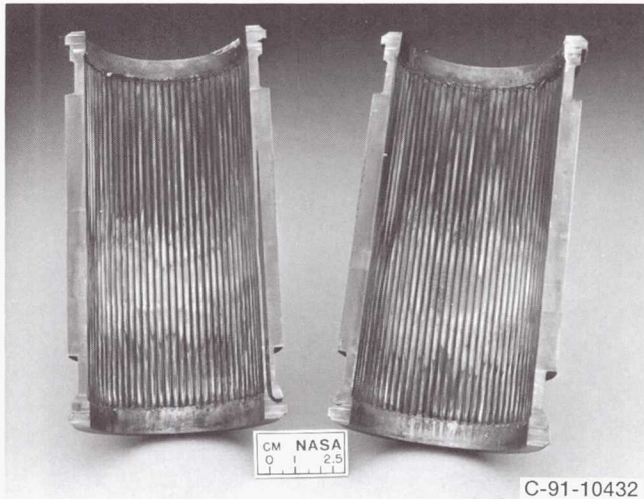
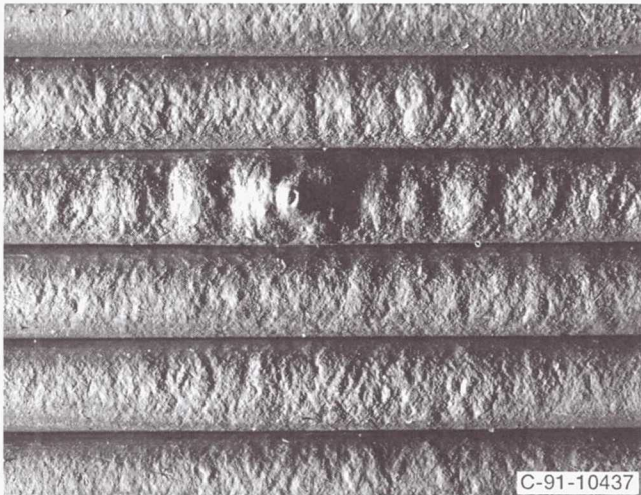


Figure 7.—Second tube chamber after 637 firing cycles.



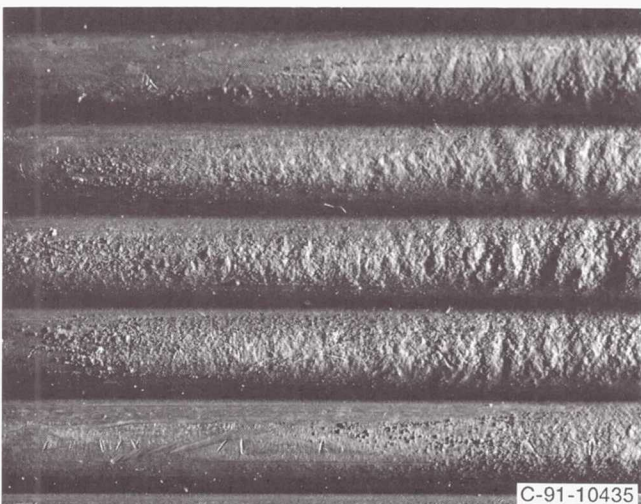
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Figure 8.—Third chamber after firing showing failure and throat area roughness.



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Figure 9.—Failure site.



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Figure 10.—Surface downstream of failure site.

thermally induced alternating compressive and tensile plastic deformations, known as thermal ratcheting. Instead, the failure would have been caused by excessive temperature and a corresponding loss of material strength with an accompanying pressure rupture. For this further reason, the testing on the second chamber was suspended, and testing was started on the third chamber.

To reduce the cost of running many cycles, a set of more severe test conditions was selected for the third chamber. These conditions were a combustion-chamber pressure of 4137 kN/m^2 (600 psia), a mixture ratio of 6.0, and a coolant flow of 0.807 kg/sec (1.78 lb/sec). Figure 6 shows that the coolant flow of 0.807 kg/sec (1.78 lb/sec) was expected to produce a crown temperature of $671 \text{ }^\circ\text{C}$ ($1240 \text{ }^\circ\text{F}$). The expected life at this temperature for the 72 milled-channel chamber configuration would be 118 cycles (ref. 5). The actual life obtained from the third chamber before it failed was 256 cycles. Figure 8 shows the inside of failed chamber number three. The distress in the throat location can be easily seen. Surface roughness measurements made on the tube centerlines in the throat region ranged from 178 to $573 \text{ } \mu\text{in.}$, with the average at $360 \text{ } \mu\text{in.}$ Referring to reference 7, this roughness corresponds to a wall temperature of $849.2 \text{ }^\circ\text{C}$ ($1560 \text{ }^\circ\text{F}$).

Figure 9 shows the failure site with lighting located close to the surface to provide good contrast of the surface texture. The failure appears as a protrusion in the surface shaped like a volcano, clearly indicating that the failure was a pressure rupture. Figure 10, taken just downstream of the location shown in the previous figure, shows the downstream end of the blanch zone of the throat. Visible on the smoother and cooler surfaces are a series of round globules, indicating the attachment of molten copper from the throat region. This molten copper is also apparent on tubes that are adjacent to the failure, indicating that even the unfailed tubes had surface melting and that the surface temperature was higher than the roughness correlation of reference 7 would predict; that is, $1083 \text{ }^\circ\text{C}$ ($1981 \text{ }^\circ\text{F}$) instead of $849.2 \text{ }^\circ\text{C}$ ($1560 \text{ }^\circ\text{F}$).

The chamber was sectioned in the region of the failure and in other areas for purposes of comparison. Each specimen was polished and etched to show the metallurgical grain structure and photographed to illustrate the findings. Figure 11 shows the failure site and the distress in the two adjacent tubes. The nature of the stress rupture failure is shown; there is no evidence of thermal ratcheting, only tensile yield. There is evidence of thinning of the wall thickness at the top of the adjacent tubes, which would indicate tensile yield of the material because of insufficient strength at the elevated temperature. The height of the coolant passage is increased due to this yielding as well. The grain structure shows a wide variation of grain size from top to bottom. The grains at the bottom are the original fine grain structure of the OFHC copper tubes. Farther up the sides of the tubes, where the temperature was higher, the grains are larger, and at the top, where the tubes were hottest, they are largest. This grain

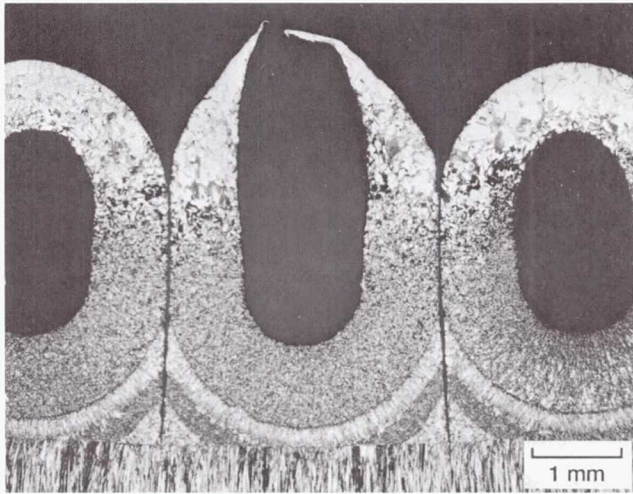


Figure 11.—Cross section at failure site.

growth is a result of exposure to the annealing environment of the excessive temperatures present on the tubes.

Figure 12 shows a region of the specimen 0.42 cm (0.16 in.) downstream from the throat in a lower temperature zone. The tensile yielding is still evident in the appearance of thinned wall thickness at the top of the tubes along with an increased height dimension of the coolant passage. The grain growth appears as severe as at the throat location, indicating exposure to excessive temperature. In addition, it can be seen that the “as-fabricated” expansion joint experienced some compressive yielding on heat-up that caused an expansion gap to form between the tubes. The width of this gap is the exact width required to fully close during the hot-fire duration of the cycle. This is exactly the kind of gap formed in the compliant chamber work of reference 8, where it was shown that a natural strain-relieving gap is formed on the first hot-fire test cycle. The absence of this expansion gap in figure 11

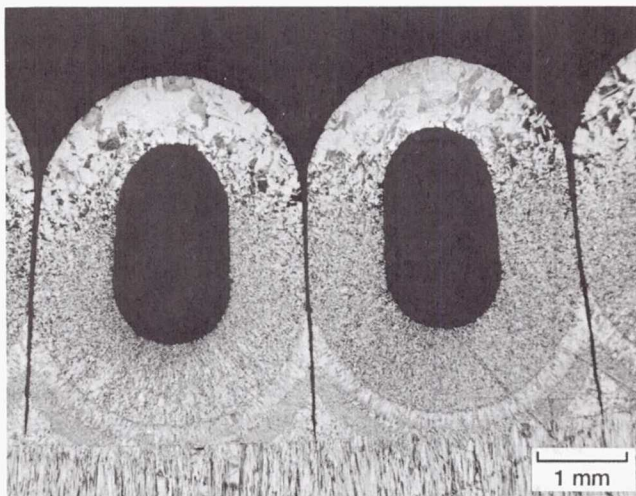


Figure 12.—Cross section 0.42 cm downstream of the throat.

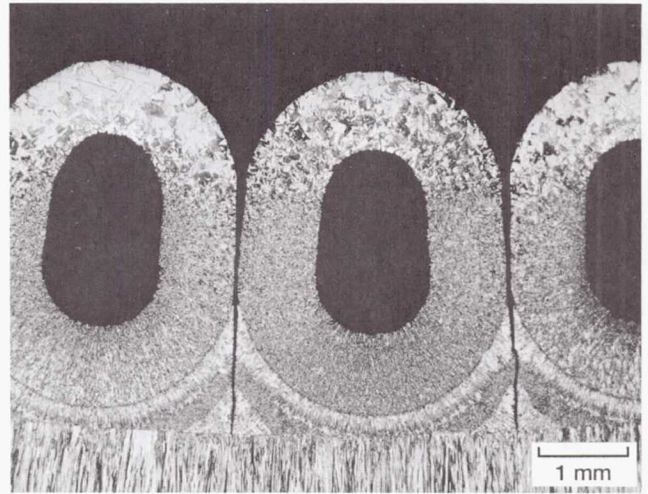


Figure 13.—Cross section 0.84 cm downstream of the throat.

may be due to the pressure rupture failure that, when it burst, caused the wall to move and close the gap. In either case the natural expansion joint of the stacked tubes did not show any crack propagation beyond the original expansion joint dimension.

Figure 13 is a photograph of the specimen taken at a location 0.84 cm (0.33 in.) downstream of the throat. The temperature at this location is low enough to result in less distress of the cooling tubes. The thinning at the top of the tubes and the increase of coolant-passage height is less severe, indicating less tensile yielding due to the coolant pressure forces. The grain growth is also less severe as the fine grain on the bottom continues farther up the sides of the tubes; the grain size at the top of the tubes is somewhat smaller than the grains of the previous specimen. Also, this figure illustrates the forming of the expansion gap in the same manner as the previous two figures. It is obvious that the feature of structural compliance performed as expected. The expansion joint between the tubes shows no crack propagation beyond the initial dimension.

The next specimen was obtained farther downstream at a location 1.27 cm (0.5 in.) downstream from the throat. Figure 14 is a photograph of this specimen, and it shows no distress at all. The grain structure is uniform from top to bottom of the tube indicating no annealing and grain growth. Furthermore, there is no evidence of yielding or coolant-passage distortion. The expansion joints appear as fabricated with no evidence of plastic deformation on the first cycle and with the bond line between tubes as originally fabricated.

In addition to metallography, several of the chamber specimens were analyzed using the scanning electron microscope (SEM). The SEM photographs (figs. 15 to 17) showed that the hot-gas-side surfaces had considerable porosity and some recast copper. The porosity and recast copper indicate that a temperature above 1083 °C (1981 °F), the melting point of copper, was obtained. These findings are similar to those

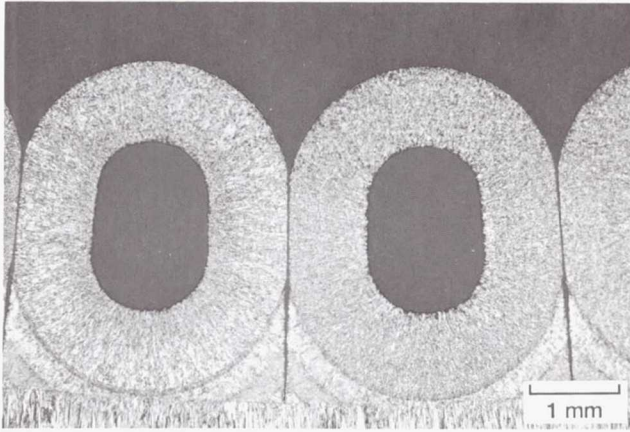


Figure 14.—Cross section 1.27 cm downstream of the throat.

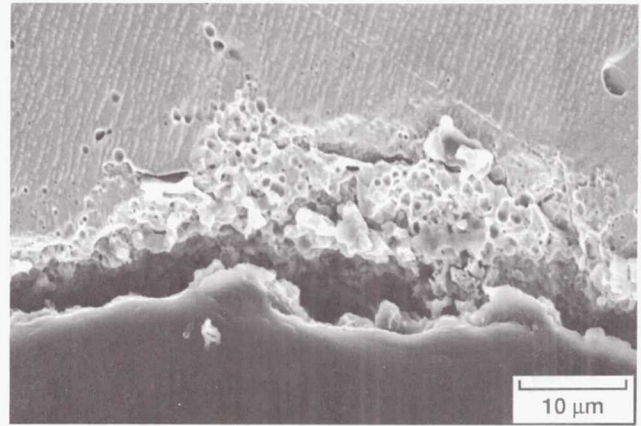


Figure 17.—SEM showing porosity and recast copper in tube away from its failure site.

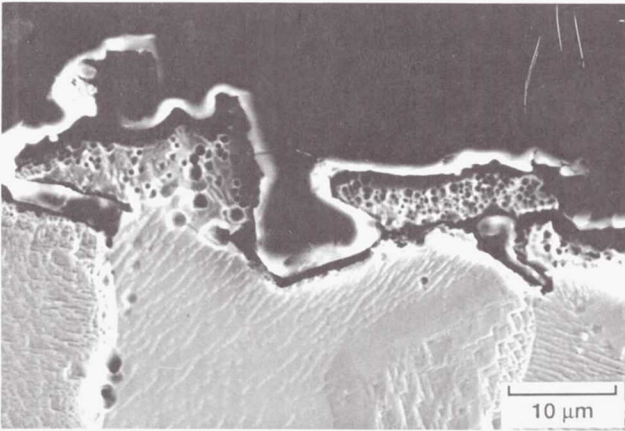


Figure 15.—SEM showing porosity and recast copper downstream of failure site.

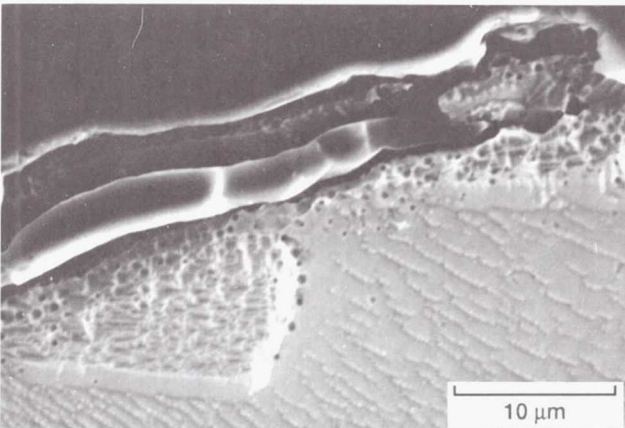


Figure 16.—SEM of area downstream of failure site.

reported in reference 7. This is further evidence that severe blanching of the tubes occurred.

Grain growth phenomena of OFHC copper were also examined in reference 8. In this work coupons of OFHC copper were subjected to a variety of temperatures. The coupons were ground, polished, etched, and then photographed to compare them with the tubular thrust chamber specimens. This was done to obtain better definition of the thermal environment to which the specimens had been exposed. Figure 18 was taken from the work reported in reference 8. This figure shows the grain size obtained after heating an OFHC copper channel 10 min at 760 °C (1400 °F). The photograph was taken at a magnification of 25×. For comparison, a sample of

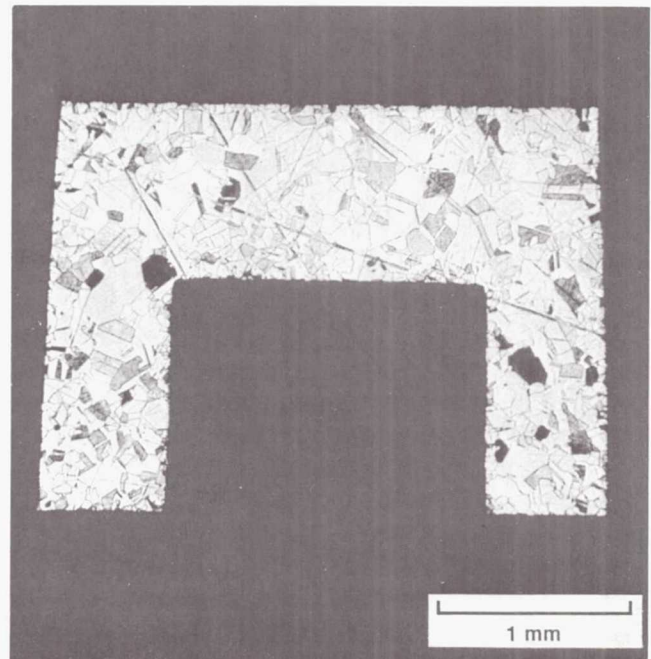


Figure 18.—Grain size specimen from annealing study, 10 min at 770 °C (1400 °F).

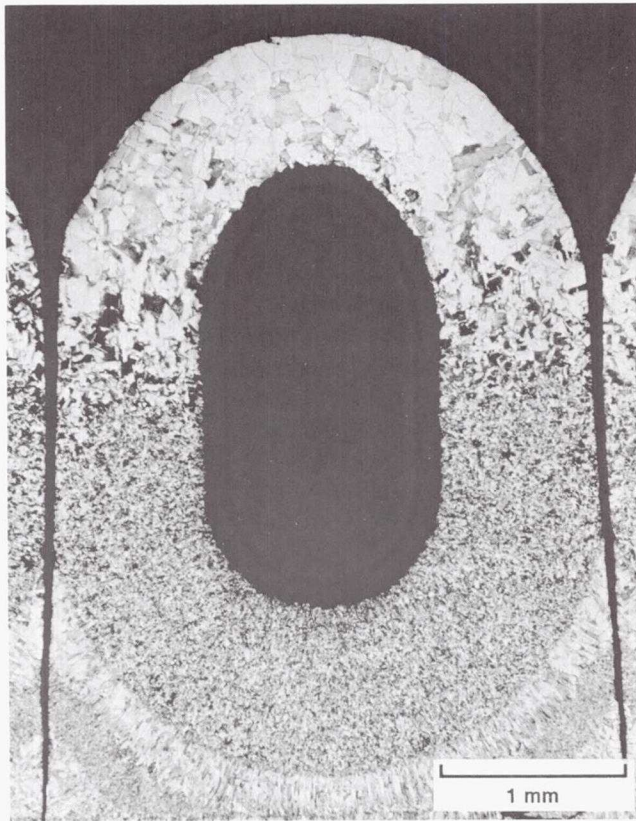


Figure 19.—Cross-section of fired tube sample taken from 0.42 cm downstream of the throat showing grain structure.

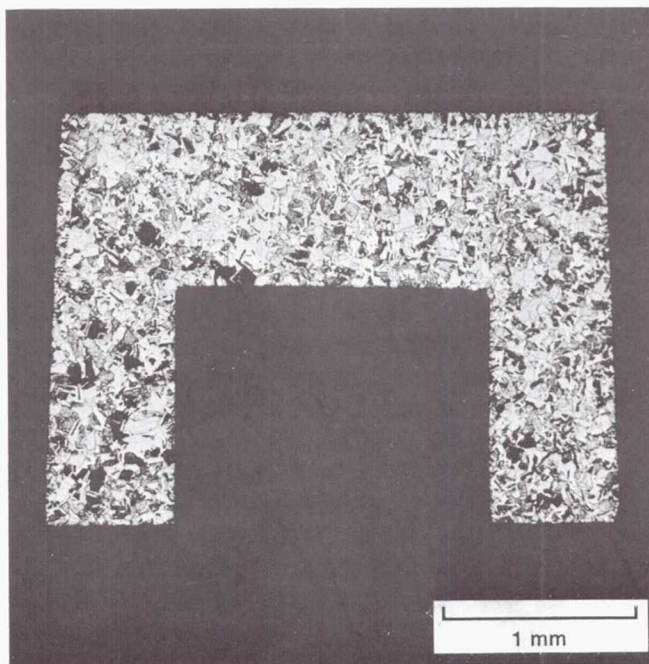


Figure 20.—Grain size specimen from annealing study, 10 min at 538 °C (1000 °F).

the wall material of a fired chamber (also photographed at 25×) is shown in figure 19. The grain sizes are equivalent, indicating that they had been exposed to the same temperature. However, the tube sample of figure 19 was taken from a location of 0.42 cm (0.16 in.) downstream of the throat area.

Figure 20 shows the grain size resulting from exposure to 538.1 °C (1000 °F). Grains of this size are also seen in figure 19, which shows an area just below the crevice region. This indicates that the temperature in the crevice region was slightly higher than 538.1 °C (1000 °F). This accounts for a crown to crevice difference of just under 204.4 °C (400 °F), which agrees reasonably well with the analytical prediction of 171.4 °C (340 °F) for this difference.

Conclusions

A subscale combustion chamber was designed incorporating advanced fabrication techniques to demonstrate the advantages of the copper tube-bundle configuration over the milled-channel liner configuration. Three chambers were successfully built. One was outfitted with intrusive instrumentation to ascertain test conditions, and the other two were fatigue tested. The fatigue tests were done at combustion pressures of 4137 kN/m² (600 psia) and propellant mixture ratios of 6.0.

The first fatigue tests were run with a liquid-hydrogen coolant flow of 0.841 kg/sec (1.98 lb/sec), which was expected to result in a temperature of 511 °C (1060 °F) at the crowns of the tubes in the throat region. The predicted life of a milled-channel combustion chamber at this temperature is 200 cycles. The fatigue tests on the tubular chamber were stopped after 637 cycles had accumulated, although there was no failure. Blanching of the hot-gas-side surface had occurred, and the metal temperature at 637 cycles was estimated to be 830 °C (1525 °F). Blanching is a self aggravating phenomena that leads to failure by increasing the metal temperature until it fails. The tubular configuration demonstrated a life of over 300 percent of that of a similar milled-channel combustion-chamber configuration.

The second fatigue test was run with a liquid-hydrogen coolant flow of 0.807 kg/sec (1.78 lb/sec). This was expected to result in a crown temperature at the throat of 671 °C (1240 °F). The predicted life of a milled-channel combustion chamber of this temperature is 118 cycles. The tubular combustion chamber failed after 256 cycles. The failure was not a typical fatigue failure since there was no evidence of any thermal ratcheting. Instead, there was a simple pressure rupture failure with only tensile yields apparent, the result of operating at excessive temperature. Surface roughening indicated that the metal temperature had exceeded 849 °C (1560 °F). Grain growth measurements at 0.42 cm (0.16 in.) downstream from the throat indicate temperature exposure in excess of 760 °C (1400 °F), and the presence of molten copper downstream of the throat indicates temperatures of

1083 °C (1981 °F), at least on the last cycle. In spite of this excessive temperature brought on by the blanching process, the life of this tube-bundle type combustion chamber exceeded the life of a comparable milled-channel liner configuration by over 100 percent (256 cycles versus 118 cycles). These results clearly demonstrate the advantages of the structural compliance that result in less strain in the tube bundle type configuration. The expected fatigue life as defined by past data, involved thermal ratcheting as the primary failure cause. The lack of any evidence of this thermal ratcheting in the tube configuration indicates a longer life, not limited by low cycle fatigue.

The method of construction used in this effort, where tubes were used to fabricate a combustion-chamber liner, demonstrated the advantages of structural compliance, which resulted in a significant extension of chamber life. A prime feature of this construction method is that it did not involve any conventional welding or brazing operations. The electroform bonding of the tubes allowed a natural expansion gap to be formed. The natural expansion joint did not weaken the structural integrity of the electroformed close-out, as no evidence of radial crack growth was found. The structural compliance of this design avoided the damage that causes conventional fatigue failures by accommodating some of the excessive thermal strains.

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National Aeronautics and Space Administration
Cleveland, Ohio, May 1, 1992

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