Survival of actively cooled microvascular polymer matrix composites under sustained thermomechanical loading

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ABSTRACT

Exposure to high heat can cause polymer matrix composites (PMC) to fail under mechanical loads easily sustained at room temperature. However, heat is removed and temperature reduced in PMCs by active cooling through an internal vascular network. Here we compare structural survival of PMCs under thermomechanical loading with and without active cooling. Microchannels are incorporated into autoclave-cured carbon fiber/epoxy composites using sacrificial fibers. Time-to-failure, material temperature, and heat removal rates are measured during simultaneous heating on one face ($5\text{--}75\text{ kW/m}^2$) and compressive loading ($100\text{--}250\text{ MPa}$). The effects of applied compressive load, heat flux, channel spacing, coolant flow rate, and channel distance from the heated surface are examined. Actively cooled composites containing 0.33% channel volume fraction survive without structural failure for longer than 30 minutes under 200 MPa compressive loading and 60 kW/m^2 heat flux. In dramatic comparison, non-cooled composites fail in less than a minute under the same loading conditions.

Keywords: B. Thermomechanical; B. High-temperature properties; D. Thermal Analysis; No category: Vascular Cooling

1) INTRODUCTION

Thermomechanical loading can be a dangerous situation for polymer matrix composites (PMC). If the material temperature exceeds the glass transition temperature (T_g), structural failure may occur under loads that would be easily sustained at room temperature [1–7]. At temperatures above T_g , matrix softening, creep, thermal stress induced damage, and combustion occur progressively with time and contribute to degradation in strength and eventual structural failure [6]. Applications in the aerospace, automotive, and marine industries may require PMCs to either operate continuously under a designed thermomechanical load or maximize survival time during fires [7]. Active cooling by pumping coolant through an embedded microvascular network removes heat and reduces temperature in PMCs [8–12]. In this study we compared the survivability (*i.e.* time before catastrophic structural failure) of actively cooled and traditional non-cooled PMCs under sustained thermomechanical loading.

Survivability of PMCs under thermomechanical loading is commonly assessed by measuring time-to-failure under constant, sustained one-sided heating with simultaneous compressive or tensile loading below the room temperature strength [1–7]. Heat is typically applied by either a torch or a radiative heater with heat fluxes in the range of approximately 10-100 kW/m² to match the heat generated by structural fires [7]. Catastrophic structural failure does not occur on the order of several hours below a threshold thermomechanical load. Above this threshold loading, the time-to-failure reduces with higher applied heat flux and mechanical load. Failure is dominated by creep in the low heat flux regime and may occur on the order of hours after the application of load. In contrast, failure is dominated by matrix flow and decomposition in the high heat flux regime and occurs on the order of just a few minutes [1,2]. Compressive (vs. tensile) loading causes failure to occur more quickly as the reduction in matrix shear stiffness at elevated temperature leads to local instability and a buckling mode of failure [1,5].

Microchannels have been fabricated in PMCs by solder removal [13,14], manual cord extraction [15–17], integration of hollow fibers [11,18–22], and the vaporization of sacrificial components (VaSC) technique [23–26]. VaSC produces microchannels by integrating sacrificial fibers, typically composed of poly(lactic acid) treated with a catalyst, that are removed at elevated temperature (ca. 200° C) under vacuum to create a hollow channel. When the channel volume fraction is small (ca. < 2%) and the structural fibers are not misaligned by the channels, channels have a negligible effect on tensile and compressive properties [20,25], interlaminar fracture toughness [15,21], and impact resistance [14,22].

Soghrati *et al.* [8,9] developed a computational framework for modeling active cooling in vascularized PMCs subjected to a constant heat flux on one surface. The effect of coolant flow rate, channel length, channel spacing, and channel trajectory on material and coolant temperature were reported. Coppola *et al.* [27] measured the flexural modulus of actively cooled glass-fiber/epoxy composites in environments above T_g . Up to 90% retention of flexural modulus was achieved with laminar coolant flow rates through 500 μ m diameter channels (3 % channel volume fraction) in a 325° C environment. Keller *et al.* [28,29] used liquid cooling through large passages in truss-like PMC slabs used in building and bridge construction to improve fire endurance. While significant

improvements in survivability were demonstrated, the liquid cooling passages were on the order of 20 cm in hydraulic diameter and the required volumes of coolant will significantly increase the structural weight.

The objective of this study is to examine the survival of laminated carbon fiber/epoxy composites under simultaneous radiant heating and compressive loading while being actively cooled. Time-to-failure of actively cooled and non-cooled control specimens is compared in a constant load, edge-supported compression test with heat flux in the range of 5-75 kW/m² applied to one face of the specimen. Compressive loads were applied in the range of 100-250 MPa and represent typical service loads corresponding to 12-29% of the estimated composite strength (ca. 850 MPa based on a first-ply failure analysis using Classical Lamination Theory and Maximum Stress Theory) at room temperature. Infrared (IR) thermography on the non-heated face is used to study the distribution of temperature, while heat removal rate is monitored by measuring the change in fluid temperature. The effect of applied heat flux, compressive load, channel spacing, channel depth from the heated face, and coolant flow rate is reported.

2) MATERIAL PREPARATION PROCEDURES

2.1) Sacrificial Fiber Synthesis

SFs were manufactured from poly(lactic acid)/tin(II) oxalate catalyst pellets (3% catalyst by weight) (CU Aerospace) using a lab-scale melt spinning apparatus according to the procedure described by Gergely *et al.* [26]. Prior to spinning, the pellets were dried at 70° C overnight. The pellets were loaded into the extruder chamber and allowed to melt at 175° C for 45 min. The melted material was then extruded through a 1.25 mm spinneret at approximately 5.5 g/min and cooled as it travelled to the take-up reel. The fiber take-up reel (88 mm diameter) was rotated at 41 rpm, which stretched the fibers to obtain a diameter of 650 µm. The fibers were then drawn down to 360 µm at 80° C on a continuous drawing machine (drawing performed by CU Aerospace).

2.2) Survivability Test Specimen Manufacture

Composite specimens were fabricated from 20 layers of prepreg (supplied by Boeing) composed of Cycom 977-3 resin (Cytec) on IM7 carbon fibers (Hexcel) with a fiber areal weight of

190 g/m² and a matrix content of 32% by weight. The nominal fiber volume fraction was 60.6%. Two laminate stacking sequences were used (Figure 1a) containing SFs oriented in the 90° direction. Layup A provides a double 90° layer thickness at the mid-plane for incorporating the SFs. Layup B is a slight variation of A that provides doubled 90° layers near the surface of the specimen. SFs (360 µm nominal diameter) were integrated into [90°]₂ plies by cutting the plies along the direction of the carbon fibers and placing the SF in the space created by the cut, according to the procedure developed by Norris *et al.* [14]. In addition, the SF diameter was selected to match the approximate thickness of the two 90° plies (final ply thickness = 180 µm). This procedure prevented misalignment of the carbon fibers in the adjacent 0° plies, which has been shown to result in reductions in compressive strength and modulus [17,20].

Composites were cured in an autoclave at 130° C for 4 h then 170°C for 3 h (2° C/min ramps) and a pressure of 620 kPa using the manufacturer's recommended vacuum bagging procedure. Following the autoclave cure, the panels were cut into individual specimens slightly larger than their final size using a water cooled diamond saw and placed in a vacuum oven for 48 h at 200° C for the VaSC treatment. An image of a typical channel post-VaSC is shown in Figure 1b. Following VaSC, specimens were finished to 84 mm x 84 mm using an end-mill. Acetone was pumped through the channels to clean out any leftover debris from machining. The surfaces were coated with high temperature black paint (Zynolyte Z635) to ensure uniform emissivity of the surface ($\epsilon = 0.95$). Thickness of the final composites ranged from 3.5-3.7 mm.

Specimens were manufactured using layup A with channels spacings of 8.5, 12, and 20 mm, which corresponded to 8, 6, and 4 total channels, respectively. Channel volume fraction (V_c) equaled 0.32%-0.34%, 0.23%-0.24%, and 0.14%-0.15% for the 8.5, 12, and 20 mm channel spacings, respectively. Channel volume fraction is calculated as

$$V_c = \frac{\pi D^2 / 4}{tw} \quad (1)$$

where D is the channel diameter, t is the composite thickness, and w is the channel spacing.

Channels were centered across the specimen width. In addition, control specimens containing no

channels were created. All specimens manufactured with layup B contained channels spaced 12 mm apart.

2.3) DMA and TGA Specimen Manufacture

Specimens for dynamic mechanical analysis (DMA) and thermogravimetric analysis (TGA) were manufactured using the same procedure as described in Section 2.2 except the layup sequence was $[90^\circ]_{16}$ and did not contain SFs. DMA specimens had a thickness of 2.9 mm and were cut to 6 mm x 40 mm with the fibers perpendicular to the longitudinal axis. TGA specimens were approximately 1 mm x 1 mm x 3 mm yielding a final specimen weight of 5-10 mg.

3) TESTING AND ANALYSIS PROCEDURES

3.1) Survivability of Composites Under Thermomechanical Loading

3.1.1) Test Setup

Figure 2 shows the details of the test setup used for thermomechanical testing. Compressive load was applied to the specimens in a modified compression-after-impact fixture (ASTM D7137) by an electromechanical testing frame (Instron Model 5984) equipped with a 150 kN load cell. This fixture applies load to the top and bottom edges of the specimen while providing support at the edges to prevent global buckling. The primary modification to the fixture was reducing the length and width of the test region to accommodate the 84 mm x 84 mm specimen (ASTM standard specimen size is 150 mm long by 100 mm wide). This smaller specimen size was used to reduce material requirements. All other dimensions were consistent with the ASTM standard and failure modes were consistent with those acceptable by the standard. Structural insulation material (Glashterm HT obtained from Red Seal Electric Company, $k = 0.27 \text{ W/m.K}$, 8 mm thick) was placed on the top and bottom loading plates to reduce heat transfer to the loading fixture. A 2 mm thick 17-4 PH stainless steel plate was placed between the composite and the insulation to preserve the hardness of the loading surface of the fixture. The anti-buckling supports were also manufactured from the structural insulation to reduce heat transfer from the specimen to the fixture.

Heat was applied to one face of the specimen by a radiative heater (Precision Control Systems Inc., Model 5080 heater and Model 5620-11-SP34 power supply), which was placed in contact with the loading fixture. The heater came with six heating lamps, however, only the bottom four lamps were used to better match the heater area to the size of the exposed specimen face. All surfaces of the fixtures in view of the heaters were covered with reflective aluminum foil tape to minimize direct heating of the fixture. The temperature profile on the opposite side of the specimen was imaged with an IR camera (FLIR, Model SC620).

The heat flux from the radiative heater was calibrated using a heat flux sensor (HFS) (Vatell, model Thermogage 1000-1, 25 mm diameter) excited using a signal-conditioning amplifier (Vatell, model AMP 15). The heater power was controlled using an external 4-20 mA signal provided by a USB voltage supply (Phidgets, model 1002_0) to the heater's power supply and a custom LabVIEW code (National Instruments). During calibration, the HFS was positioned in a hole centered on the face of an 84 mm x 84 mm x 3.5 mm aluminum copy of the composite test specimen. The aluminum copy was painted with black paint (Zynolyte Z635) to match the surface emissivity of the actual test sample and emulate the radiative heat transfer environment surrounding the sample during a test. As heat was applied to the HFS, the signal from the HFS was recorded for each value of heater controlled current. The voltage signal from the HFS was then translated into the applied heat flux using data provided by the manufacturer. This procedure resulted in a relationship between heater controller current and applied heat flux.

Coolant was pumped through each channel independently in actively cooled specimens using a peristaltic pump containing up to eight pump heads (Cole-Parmer, Model EW-07551-00), ensuring equal flow in each channel. Distilled water maintained at 20° C by a temperature-controller circulator (Julabo, Model F32-HP) was used as the coolant. Coolant was delivered via 1/4 inch OD, 1/8 inch ID silicone rubber tubing connected to the channels using 21 gage stainless steel syringe tips inserted approximately 1 mm into a hole drilled in the end of the channel. The outlet fluid temperature of each channel was measured using type T thermocouples (Omega Engineering, Model TMQSS-020U). Thermocouples were positioned in the middle of the flow path by inserting into a hole drilled in a 1/8" straight barbed polycarbonate fitting and then sealing them in place with

epoxy adhesive. Thermocouple data was collected using several four-input temperature sensors (Phidgets, Model 1048) and recorded using a custom LabVIEW code (National Instruments) at 1 Hz.

3.1.2) Test Procedure

During survivability testing, the pump was first turned on to circulate coolant through the specimen and allow the temperature and flow rate to equilibrate. Compressive stress was then applied at a crosshead displacement rate of 1 mm/min to the prescribed load and held constant for the remainder of the test. Finally, the radiant heater was turned on to the prescribed heat flux and held constant until failure occurred. An abrupt drop in load to near zero signaled specimen failure.

3.1.3) Data Analysis

Time-to-failure was recorded as the time between the application of heat and failure of the specimen. The cooled heat flux from the specimen was calculated with knowledge of the coolant temperature as

$$q''_{AC} = \frac{\sum_{i=1}^n (Q_c \rho c_p \Delta T)_i}{A_s} \quad (2)$$

where Q_c is the volumetric flow rate of the coolant in each channel, ρ is the density of the fluid, c_p is the specific heat capacity of the fluid, ΔT is the change in fluid temperature from inlet to outlet for each channel, n is the number of channels, and A_s is the surface area exposed to the heated face. Density and specific heat capacity were taken from tabulated data using average fluid temperature based on inlet and outlet fluid temperature. Cooling efficiency is defined as the ratio of supplied heat flux to cooled heat flux,

$$\eta = \frac{q''_{AC}}{q''_s} \quad (3)$$

where q''_s is the supplied heat flux to specimen.

3.2) DMA and TGA Testing Procedure

Composites were tested following ASTM D7028 in three-point bending on a DMA (TA Instruments, Model RSAIII) at 1 Hz to a maximum strain of 0.1% on a 40 mm span. The

temperature was ramped at 5° C/min from 25-300° C in a nitrogen environment. T_g was recorded using a step transition analysis of the storage modulus (E'). Composite samples were also tested by TGA (Mettler Toledo, Model TGA/DSC 1) from 25-600° C at a heating rate of 5° C/min in a nitrogen environment (35 ml/min flow rate) in order to assess thermal stability. Two tests were run for both DMA and TGA tests for repeatability.

4) RESULTS AND DISCUSSION

4.1) Dynamic Mechanical Analysis and Thermogravimetric Analysis

DMA and TGA results are shown in Figure 3. DMA testing revealed two transitions in the thermomechanical response. The onset of a loss in storage modulus is observed at approximately 175° C along with a rapid increase in both the loss modulus and $\tan(\delta)$. The storage modulus began to drop at a faster rate at approximately 225° C, while the loss modulus and $\tan(\delta)$ reached peak values shortly thereafter. The presence of multiple transitions is likely due to the toughening phase present in the matrix material. T_g evaluated by a step transition analysis on the storage modulus from 150° C to 235° C yields a value of 217° C. If the peak in $\tan(\delta)$ is used then T_g is roughly 237° C. Mass loss was detected by the TGA beginning at approximately 275° C and reached 5% at 339° C, followed by a steep drop in mass thereafter. Remaining mass at the end of the test is composed of the carbon fibers and char from the decomposed matrix.

4.2) Survivability of Actively Cooled Composite Under Thermomechanical Loading

4.2.1) Effect of Channel Spacing and Applied Heat Flux

Active cooling greatly increases the survivability of composites subjected to thermomechanical loading (Figure 4). Figure 4a compares time-to-failure for control (non-cooled) and actively cooled (layup A) composites at three different channel spacings. The 200 MPa load represents a typical service load for a composite at room temperature and is 24% of the estimated room temperature compression strength. Control composites failed rapidly at heat fluxes above 5 kW/m², while actively cooled composites were able to survive at 25, 45, and 60 kW/m² for the 20, 12, and 8.5 mm channel spacings, respectively. Above these values time-to-failure decreased non-linearly with

increased heat flux. Reducing channel spacing dramatically increased time-to-failure at a given value of supplied heat flux. Channel architecture is an important design factor for increasing active cooling performance, as was observed by previous studies [8,9,27].

The effect of supplied heat flux on the maximum temperature of the non-heated face at failure is presented in Figure 4b. Control samples reliably failed when the maximum temperature reached T_g . Failure did not occur in the sample tested at 5 kW/m², which only reached 171° C. Failure was more complex in the actively cooled specimens as there was no clear correlation between maximum temperature and failure. Although, surface temperature in specimens that failed was higher than in those that did not fail, the increase in maximum temperature weakly correlated to increasing supplied heat flux. Maximum surface temperature was decreased with reduced channel spacing at a given value of supplied heat flux.

Figure 5 shows maximum temperature vs. time and representative IR temperature profiles for control and actively cooled specimens at several values of supplied heat flux. Temperature increased at a faster rate with higher heat flux in all cases. In control specimens, temperature was highest in the center of the heated region and cooler at the edges (Figure 5c). Control specimen temperature increased monotonically until reaching T_g and then failure occurred immediately. In contrast, actively cooled specimen temperature increased rapidly then remained fairly constant until failure occurred or the test was concluded. The long delay between reaching maximum temperature and failure is likely a result of creep, plastic deformation, and progressive damage formation, as was observed by Bausano *et al.* [1] during combined compression/one-sided heating tests on non-cooled composites under similar heat fluxes. During the delay period before final failure, movement of the crosshead was observed that implies material softening and audible cracking was heard indicative acoustic emission from damage formation.

Figure 5d shows that actively cooled specimen temperature was initially hotter towards the center and cooler towards the edges with a reduction in temperature near the channel locations. Figure 5e shows the temperature profile on the same specimen after 4 minutes of testing and reveals that significant cooling occurred in the center of the specimen. This region of the specimen

corresponds to the location of charred material on the opposite (heated) face discovered following the test. Charring occurred on the heated face during all tests where the supplied heat flux exceeded 30 kW/m^2 . Volatization was indicated by visible smoke rising from the heated area and coincided with the beginning of the temperature plateau. After the matrix burned away, only carbon fibers and decomposed matrix remained (see section 4.2.5 for image showing the charred region). The temperature reduction likely occurred as a result of locally reduced heat conduction through the thickness as a result of reduced effective thermal conductivity (from the formation of porosity and cracking during char formation) and energy consumption by the chemical reactions during volatization. This reduction in temperature may have extended the life of the composite by decreasing the temperature of the underlying material.

The effect of supplied heat flux on the cooled heat flux and cooling efficiency at failure is presented in Figure 6a and Figure 6b, respectively. Initially the cooled heat flux increased with supplied heat flux, but then plateaued at high heat flux conditions. However, this plateau did not correspond to saturation of the coolant with heat (which would result in the coolant boiling), as the maximum outlet coolant temperature for any architecture at the prescribed flow rate was only 50°C for all of these tests. Rather, the plateau corresponds to the maximum cooled heat flux achievable for each channel architecture and increased with decreased channel spacing. Cooling efficiency declined as a function of increased supplied heat flux and was significantly lower in specimens that failed than in specimens that survived the full thirty minutes. Measured cooling efficiencies in the range of 65-75% indicate that most of the supplied energy was removed by the coolant in these tests rather than through conduction to the fixture or radiation/convection to the surrounding environment. Reduced channel spacing improved both cooled heat flux and cooling efficiency at a given supplied heat flux. Figure 6c shows cooled heat flux vs. time for several values of supplied heat flux. Cooled heat flux increased at a faster rate with increased supplied heat flux and eventually reached a steady state where it remained until failure or the test was ended, as was observed for maximum temperature.

4.2.2) Effect of Flow Rate

The effect of flow rate on survivability and cooling performance is presented in Figure 7. Time-to-failure increased with increased flow rate, with the specimen failing after only 42s at 0 ml/min and after 176s at 135 ml/min. The temperature at failure only moderately reduced as flow rate was increased, with the largest drop occurring between 0 ml/min and 22.5 ml/min. The benefit of increased flow rate is best revealed by the dramatic increase in cooled heat flux, which corresponds to the increase in time-to-failure and reduced temperature. As the flow rate increased, the coolant temperature decreased. The larger temperature gradient between the composite channel wall and the coolant corresponds to the increase in cooled heat flux. While increased flow rate corresponded to increased time-to-failure, it also increases the required pumping power with the square of the flow rate [27]. By increasing the flow rate from 3.33 ml/min per channel to 22.5 ml/min per channel the time-to-failure was increased by approximately 2 minutes, but the required pumping power was increased by up to a factor of 45 (see [27] for description of pumping power calculation). The optimal designs will balance the cooling efficiency with required pumping powers for the target application.

4.2.3) Effect of Applied Compressive Stress

The effect of compressive loading on the time-to-failure is revealed in Figure 8. Control samples tested at 15 kW/m² failed at all four values of applied loading and time-to-failure was only slightly reduced with increased load. For the actively cooled samples, failure did not occur at either 55 or 65 kW/m² at 100 MPa (11.7% of the estimated room temperature strength). We hypothesize that the load was carried by the cooler regions at the edges of the sample (see Figure 5d). At higher loads, the time-to-failure decreased with increased compressive stress. In addition, samples failed faster at 65 kW/m² than at 55 kW/m² at the same compressive load as was previously observed.

4.2.4) Effect of Channel Depth from Heated Surface

Placement of the channels near the heated surface resulted in unexpected behavior. Figure 9 shows a comparison of the average temperature on the non-heated face as a function of time for layup A and B specimens at 70 kW/m². Temperature increased monotonically in Layup A and the

specimen failed rapidly. In stark contrast, layup B did not fail and has a sudden reduction in temperature around 70 s, then a relatively constant temperature profile for the remainder of the 30 min test. The temperature drop was accompanied by water pooling at the base of the specimen on the fixture and visible steam rising. The vaporization of water to steam provides an important and substantial heat removal mechanism. The heat flux removed by vaporization is equal to $q_e'' = Q_e \rho h_v$ where Q_e is the volumetric rate of water vaporizing and h_v is the enthalpy of vaporization of the water (2260 J/g). If just 1% of the total volumetric flow rate of water entering the specimen vaporized, the cooled heat flux would be increased by 9.4 kW/m². At 5% the cooled heat flux is increased by 47 kW/m². The thermal images in Figure 9 show that the reduction in temperature (as low as 70° C) was particularly dramatic near the center of the specimen coinciding with the location of a crack visible on the heated surface where water was able to escape from the channels. Tests conducted at a supplied heat flux of 60, 65, and 75 kW/m² at 200 MPa applied stress, as well as at 65 kW/m² at 250 MPa applied stress, all yielded the same behavior and did not fail within 30 minutes. The enhancement to heat removal (and therefore survivability) from the release of fluid onto the surface could be achieved in actively cooled composites that utilize surface evaporation for cooling under excessively high heat loads.

4.2.5) Damage and Failure Modes

Typical damage patterns following survivability testing of control layup A, vascular layup A, and vascular layup B specimens are shown in Figure 10. Control specimens failed in the hottest region located at the center of the specimen along the loaded axis, as shown in Figure 5c. Failure occurred as a result of localized damage in the form of shear cracking and delaminations. In contrast, damage in the vascularized layup A sample covered a much larger area. Charring of the matrix resulted in dry carbon fibers visible on the heated surface. Below the burned layer is a region of large delaminations and shear cracking. The majority of damage is located near the hottest region at the center of the specimen, but some delaminations extended the length of the specimen. The cross-section of the vascularized layup B sample shows that damage was limited to the region between the heated face and the layers containing the channels and consisted of a large delamination intersecting with the channels and a transverse crack bridging the delamination and

the heated surface. The water observed on the specimen during testing was released through this cracking formation.

5) CONCLUSIONS

Survival of actively cooled composites subjected to simultaneous compression and one-sided heating was measured and compared to traditional (non-cooled) composites. Vascular specimens with 0.34% channel volume fraction or less were tested with channels located at the mid-plane or near to the heated surface. Actively cooled composites with mid-plane channels survived longer than thirty minutes at heat fluxes up to 60 kW/m² and compressive loading of 200 MPa (ca. 24% of the room temperature strength), whereas traditional composites failed in ten minutes or less at heat fluxes as low as 10 kW/m². When channels were placed near the heated surface, cracks formed that connected channels to the heated surface, releasing water onto the surface that prevented specimen failure for at least 30 minutes in all tests conducted. During fires, heat fluxes in the range of 10-100 kW/m² are generated. Actively cooled composites are therefore able to greatly improve the safety of composite structures during fires by either extending the time before or preventing structural collapse.

Survivability in actively cooled specimens containing channels at the mid-plane was evaluated as a function of supplied heat flux, compressive load applied, channel spacing, and coolant flow rate. Higher heat flux and compressive load corresponded to faster time-to-failure. Time-to-failure increased with reduced channel spacing and increased coolant flow rate, corresponding to increased cooled heat flux and cooling efficiency. However, reduced channel spacing caused a much more substantial increase in survivability than increased coolant flow rate. Damage in actively cooled composites was more complex and covered a larger area of the specimen compared to traditional composites. Traditional (non-cooled) composites heated rapidly and monotonically until failure occurred. In contrast, actively cooled composites initially heated rapidly, but then remained at fairly constant temperature until failure occurred or the test was stopped. During the temperature plateau, burning occurred on the heated face of specimens (at $q_s'' \geq 30$ kW/m²), leading to reduced temperature on the non-heated face as a result of the combustion damage.

By properly designing an actively cooled composite's channel spacing and coolant flow rate, the service load of PMCs can be increased to higher heat fluxes than are currently possible with traditional composites without changing matrix composition. The channel architecture (e.g. channel spacing) and the coolant flow rate must be selected to maximize cooling performance while minimizing required pumping power and the effect of the channels on mechanical performance.

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FIGURES

Figure 1: a) Schematics of the layup order used for thermomechanical testing of actively cooled composites. Layup A placed the channels at the mid-plane at $D = 1.75$ mm from the heated surface, while B placed them nearer to the surface at $D = 0.75$ mm. In addition, control specimens not containing SFs were created with layup A. b) Optical image (10x objective) of a hollow channel in a carbon fiber specimen following VaSC.

Figure 2: Thermomechanical test setup. a) Schematic of the test concept. Load is applied to the top and bottom edges of the specimen, while heat is applied to one face. Heat is removed by coolant pumped through straight, parallel channels running perpendicular to the loading axis. The non-heated face is open to ambient air and the temperature field is imaged with an IR camera. b) Schematic of the loading fixture showing the placement of the specimen between the top and bottom fixtures, which apply load while providing anti-buckling support. Heat is applied to one face by a radiative heater. Slots machined into the bottom fixture allow for access to the channel inlets and outlets. Stainless steel components are shown in grey and those composed of glass-fiber based structural insulation are shown in green.

Figure 3: Thermal and mechanical characterization of the epoxy matrix. (a) DMA testing results showing the storage modulus (E'), the loss modulus (E'') and $\tan(\delta)$ vs. temperature. (b) TGA testing results showing percent remaining mass vs. temperature.

Figure 4: Thermomechanical performance of actively cooled and control specimens showing (a) time-to-failure, (b) maximum temperature on the non-heated face. Tests were conducted up to 30 minutes, after which the test is reported as “did not fail”. Results from these tests are highlighted in light blue. Compressive load was 200 MPa and total coolant flow rate was 90 ml/min, evenly distributed among the channels. Each data point represents the results from a single test.

Figure 5: Maximum temperature on the non-heated face (side directed towards the IR camera) as a function of time for (a) control and (b) actively cooled specimens. Representative IR images at times marked on (a) and (b) show: (c) a control specimen at 15 kW/m² shortly before failure and (d) an actively cooled specimen at 55 kW/m² after reaching maximum temperature and (e) several minutes into the test. Actively cooled specimens were manufactured using layup A, contained channels spaced at 12 mm, and were tested at 200 MPa with a total coolant flow rate of 90 ml/min.

Figure 6: Heat removal data for active cooling tests. a) Cooled heat flux at steady state as a function of supplied heat flux. b) Cooling efficiency as a function of supplied heat flux. c) Cooled heat flux as a function of time for several values of supplied heat flux. Specimens were manufactured using layup A, contained channels spaced at 12 mm, and tested at 200 MPa with a total coolant flow rate of 90 ml/min. Each data point represents the results from a single test.

Figure 7: The effect of total coolant flow rate on (a) time-to-failure and maximum temperature on the non-heated face and (b) cooled heat flux at failure and outlet fluid temperature. Specimens were manufactured using layup A, contained channels spaced at 12 mm, and tested at 200 MPa and 60 kW/m². Each data point represents the results from a single test.

Figure 8: Time-to-failure as a function of compressive loading for control and actively cooled specimens at a few values of supplied heat flux. Specimens were manufactured using layup A, contained channels spaced at 12 mm, and were tested with a total coolant flow rate of 90 ml/min. Each data point represents the results from a single test.

Figure 9: Comparison showing average temperature on the non-heated surface as a function of time for layup A and B. Layup A failed after only 105s, while layup B did not fail within the 30 minute testing window. Images of the temperature profile for layup B are given in IR images (1)-(3) at instances marked on the graph. Both tests were conducted at supplied heat flux of 70 kW/m^2 , applied compressive stress of 200 MPa, and total coolant flow rate of 90 ml/min.

Figure 10: Representative cross sections showing damage in each type of composite after testing. Examples of each common damage mode are marked. Composites were loaded to 200 MPa. Channels in actively cooled specimens were spaced 12 mm apart and total flow rate was 90 ml/min. Note that failure did not occur in layup B. Cross sections were taken from the center of the specimen parallel to the loading direction. The heated surface is the top surface in each image. Composites were lightly sanded with 400 grit wet sand paper then photographed. Scale bar represents 5 mm.