MICROVASCULAR COMPOSITES AS A MULTIFUNCTIONAL
MATERIAL FOR ELECTRIC VEHICLES

BY

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ABSTRACT

Electric vehicle (EV) batteries require both thermal regulation and structural protection. A novel form of battery packaging is proposed to accomplish this multifunctional objective using microvascular fiber-reinforced composites. Batteries are sandwiched between carbon fiber/epoxy panels containing embedded microchannels. Coolant flow enables active cooling while the composite material provides strength, stiffness, and crashworthiness at low weight.

Several studies are performed on the cooling performance of microvascular panels. First, thermography experiments and computational fluid dynamics (CFD) simulations are used to characterize the cooling performance of carbon/epoxy panels with straight 500 µm diameter microchannels. Panels with high enough channel density and coolant flow rate can sufficiently cool batteries below 40 °C for applied heat fluxes up to 1000 W m⁻². Next, experiments and simulations are performed on carbon/epoxy panels with more complex 2D microvascular networks (parallel, bifurcating, serpentine, and spiral networks). The spiral network offers optimal thermal performance at high pumping pressure (>100 kPa), while the bifurcating network and a computationally optimized parallel network offer slightly reduced thermal performance at much lower pumping pressure (<10 kPa).

In a collaborative effort, gradient-based optimization is used to improve the blockage tolerance of microvascular networks. Optimizations are performed on networks with different nodal degree (a measure of redundancy) to minimize temperature while the network is subject to a blockage. Tests on microvascular silicone panels confirm that optimized networks with high nodal degree experience minimal temperature rises when blocked.

Finally, crush tests are performed on microvascular carbon/epoxy panels to demonstrate how channels affect composite crashworthiness. Corrugated panels are fabricated containing 400 µm channels at different spacing (10 mm and 1.2 mm), orientation with respect to the load (transverse and longitudinal), and orientation with respect to surrounding plies. Crush tests revealed no significant reduction in specific energy absorbed (SEA) for all test cases. Further tests demonstrate that microchannels can actually improve composite crashworthiness by triggering stable, energy-absorbing failure modes in flat panels.
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CHAPTER 1

INTRODUCTION

1.1. Motivation for the use of microvascular composites in electric vehicles

1.1.1. Requirements for battery packs in electric vehicles

The global adoption of internal combustion engine vehicles has led to increasing pollution, CO$_2$ emission, and a global dependence on fossil fuels [1]. Battery-powered electric vehicles (EVs) serve as a promising alternative and have seen rising commercial success in the last five years [2]. Prismatic and cylindrical lithium-ion batteries currently dominate the EV market due to their high energy and power density compared to other rechargeable batteries [3]. Battery packs have been manufactured with capacities up to 100 kWh (for the 2016 Tesla Model S 100D) which corresponds to an estimated driving range of 540 km (335 miles) [4].

The first requirement for an EV battery pack is to provide sufficient capacity while minimizing weight, since less weight translates to farther range (for a given capacity). An ideal battery pack should have both lightweight batteries and lightweight packaging surrounding the batteries. Current battery packs are very heavy due to both the low energy density of lithium-ion batteries (ca. 250 Wh/kg compared to ca. 12,000 Wh/kg for gasoline) and the use of heavy packaging materials such as steel [3]. For example, the 85 kWh battery pack of the 2014 Tesla Model S weighs 540 kg (1200 lb) compared to the total car weight of 2100 kg (4600 lb) [5]. Work is ongoing to develop lighter batteries and battery packaging [6]. Efforts have also begun to reduce total EV weight by building the chassis out of fiber-reinforced composites instead of metals. For example, the BMW i3 uses a carbon fiber/epoxy passenger compartment and weighs ca. 200 kg less than similarly sized EVs such as the Nissan Leaf [7].

The second requirement for an EV battery pack is to provide temperature control for the batteries inside. Temperature must be regulated for lithium-ion batteries since operating temperatures outside of the range 10 – 40 °C leads to poor cycling performance and reduced lifetime [8,9]. Several thermal loads must be combatted during operation. Batteries produce in the range of 1 – 30 W of waste heat during deep cycling, with the highest heat generation occurring at high discharge rate and high degree of
discharge [10–13]. Batteries are subject to heating and cooling from operation in warm or cold climates [14]. Finally, cell damage can lead to sudden increases in temperature that can potentially lead to thermal runaway and fire [15,16].

The final requirement for battery packaging is to protect battery cells during a crash. Even minor battery damage can result in significant cost since EV battery packs are expensive and difficult to repair [5]. Major battery damage presents a critical safety issue due to the risk of thermal runaway and fire, especially if the cooling system is damaged which can lead to chemical reactions between the batteries and coolant [17,18].

1.1.2. Existing battery packaging for transportation applications

In order to maintain optimal performance and protect batteries from thermal runaway, most EVs employ an active thermal management system using either air [19–21] or a liquid coolant [22–24]. For example, the Chevrolet Volt battery pack (Fig. 1.1a) contains a liquid cooling system using water/ethylene glycol coolant [22,23]. Coolant is pumped through 0.75 mm x 1 mm channels in aluminum cooling panels that are stacked between prismatic Li-ion battery cells (Fig. 1.1b). In contrast, the Nissan Leaf employs air cooling which provides less weight and complexity but leads to poorer thermal control [5]. Alternate battery cooling schemes have also been proposed using heat pipes [25,26] or phase change materials [27].

Fig. 1.1. Battery packaging in the Chevrolet Volt. a) Schematic of the vehicle showing the T-shaped battery pack in the center (image from [28]). b) Schematic of the battery pack. Prismatic battery cells are stacked between aluminum cooling fins, fiberglass casings, and steel end plates (image from [29]).
Battery packs typically contain several protective layers to protect batteries from damage in a crash. In the Chevrolet Volt, battery cells are surrounded with glass-reinforced nylon casings which are themselves surrounded with steel (Fig. 1.1b). Despite this complex protection scheme, side-impact tests on the 2011 Chevrolet Volt led to thermal runaway and fire due to damaged batteries and cooling panels [17,18]. A recall was performed to further reinforce the pack with more steel [30]. Several Tesla vehicles have also caught on fire during highway crashes in recent years [31]. Recent studies have sought to improve battery pack crashworthiness by testing the crash response of batteries at the cell, module, and pack level [32]. Nevertheless, a lightweight battery packaging solution has yet to emerge that can seamlessly provide thermal management and crashworthiness.

1.1.3. Battery cooling concept using microvascular composites

Here we propose a novel battery packaging scheme where a single material provides both cooling and lightweight crash protection. Batteries are embedded within microvascular carbon fiber/epoxy composites through which coolant can flow (Fig. 1.2a). Batteries can be stacked into a single pack (Fig. 1.2b), with additional composite reinforcement seamlessly added around the pack exterior as needed. Alternatively, batteries can be embedded within composite body panels to achieve system-level savings of volume and weight [33,34].

![Fig. 1.2. Schematic of battery packaging using microvascular composite panels. a) Battery cell is sandwiched between two microvascular composite panels through which coolant flows. b) Assembly of multiple battery cells with a coolant distribution manifold.](image-url)
This packaging scheme fits well with the increasing use of fiber-reinforced composites in EVs due to their low weight and high strength, stiffness, and energy absorption in a crash [35,36]. In a crash, carbon fiber composites fail through a combination of compressive failure and delaminations that leads to material disintegration and high energy absorption [37–39]. The specific energy absorption (SEA) of carbon fiber composites, i.e. the energy absorbed by a composite in a crash per unit weight, ranges from 50 – 100 kJ/kg [40]. In contrast, typical metal SEA values are ca. 30 kJ/kg for aluminum and ca. 20 kJ/kg for steel [40]. Composite battery packaging can thus provide significantly increased crashworthiness compared to metals while also reducing weight. Reduced weight then leads to farther driving range, as seen in EVs that utilize carbon/epoxy composites such as the BMW i3 [37].

The composite cooling panels developed here fit within a large body of literature on the cooling of other heat sources such as fuel cells [41], microelectronics [42], antennas [43], and satellites [44]. Cooling panels made with traditional materials such as aluminum have been studied in detail for these systems, and the conductive and convective heat transfer mechanisms in such panels are well known [45]. However, this work is the first to develop a large-scale cooling panel using a lightweight, structural composite material. This study also lays a framework for developing composite materials that integrate other active and passive cooling methods, such as air cooling [46], phase change materials [47], heat pipes [48], refrigeration systems [49], thermoelectric materials [50], and the use of high surface area for radiative heat loss [51].

1.2. Literature review

1.2.1. Microvascular fiber-reinforced composites

The development of microvascular fiber-reinforced composites over the past decade was inspired by natural structural materials with vascular networks. For example, tree branches contain xylem channels for transporting water and minerals up from roots and phloem channels for transporting glucose down from leaves (Fig. 1.3a) [52]. Leaves contain dense, redundant vasculature to allow for unobstructed fluid transport even when the network is damaged (Fig. 1.3b) [53,54]. The human cardiovascular system contains a hierarchical system of arteries, veins, and capillaries to provide nutrient transport, thermal
regulation, and damage indication and healing [55]. Blood vessels allow for healing of materials as flexible as skin (Fig. 1.3c) [56] and as stiff as bone (Fig. 1.3d) [57,58].

Fig. 1.3. Vascular networks in nature. a) Schematic of vascular xylem and phloem networks in tree branches (image from [52]). b) Vascular network of a lemon leaf filled with dye for visualization. A single blockage was introduced into the central vein before dye flow (image reproduced from [54] with permission from the American Physical Society). c) Schematic of a cut in human skin being filled with blood from capillaries (image reproduced from [56] with permission from Nature Publishing Group). d) Schematic of the healing of a bone fracture (image from [58]).

Several techniques have been developed to fabricate microvascular polymers and composites as reviewed in [59]. Microvascular polymers were first created by direct-writing wax preforms, embedding the preforms into a thermoset matrix, and evacuating the preforms after cure using mild heat (30 - 90 °C) and vacuum [60,61]. This technique is effective for low-temperature curing thermosets but impractical for high-temperature curing fiber-reinforced composites. Microchannels have been fabricated in composites using hollow glass fibers [62], melt extraction of solder [63], and manual extraction of steel [64], silicone [65], and nylon [66] fibers. These techniques are rapid but are not scalable and can only produce straight channels.
The invention of the Vaporization of Sacrificial Components (VaSC) technique in 2011 [67,68] led to a breakthrough in the manufacturing of vascular composites. Preforms of polylactide (PLA) infused with a tin oxalate (SnOx) catalyst are incorporated into a composite layup and vaporized at ca. 200 °C and vacuum after resin cure. PLA fibers, laser-cut PLA sheets, and 3D-printed PLA preforms can be used to create channels with 1D, 2D, and 3D connectivity (Fig. 1.4) [69]. The VaSC process is scalable and compatible with commercial manufacturing techniques. For example, PLA fibers can be directly integrated into fabric preforms to form composites with straight or sinusoidal channels [67].

![Fig. 1.4. Complex microvascular networks made with VaSC of polylactide (PLA). a) Bifurcating template made from laser-cutting a PLA sheet and b) vascular epoxy created using the template. c - d) Tree-like 3D-printed PLA template and e) vascular epoxy created with the template (images reproduced from [69] with permission from John Wiley and Sons).](image)

Once fabricated, vasculature can provide composites with functions similar to those seen in nature. The main function studied to date is self-healing through vascular delivery of liquid healing agents [70]. Healing has been demonstrated for several damage modes such as mode 1 fracture [71], impact [72,73], and tension [64]. Damage sensing has been achieved using dyed liquid for visualization [74] and pressure sensors inside channels [75]. The electromagnetic properties of a composite have been altered by flowing liquid metal or a ferrofluid through channels [67,43]. Finally, several studies have investigated vasculature for thermal management as discussed next.
1.2.2. Cooling applications using microvascular composites

1.2.2.1. Experimental cooling studies

Early work on microvascular thermal management focused on heating and cooling unreinforced polymers. Kozola et al. tested the cooling performance of microvascular epoxy and demonstrated that dense arrays of small-diameter channels outperform arrays of larger-diameter channels [60]. Hansen et al. fabricated epoxy samples that contained two microvascular networks for healing agent flow and one network for heated fluid flow [61]. Heated samples were able to heal cracks in 1 hr vs. 40 hr without heating due to accelerated healing agent cure. Cortes et al. triggered shape reconfiguration in shape memory polymers used heated fluid flow through microchannels [76]. Significant reconfiguration was obtained (25° bending) and the performance matched well with thermomechanical simulations.

Thermal management of fiber-reinforced composites has been studied more recently. The heating of a carbon/epoxy composite was first studied by Phillips et al. [77], who used experiments and modeling to characterize the heat transfer coefficient (h) of a panel subject to heated water flow. McElroy et al. tested a carbon/epoxy panel heated with external hot air flow and cooled with air flow through microchannels [78]. Heat transfer simulations were validated by experiment and used to optimize channel design for film cooling of turbine blades. Coppola et al. performed cooling tests on a hybrid composite consisting of microvascular glass-fiber/epoxy bonded to microvascular nickel titanium (NiTi). The uncooled composite exceeded its maximum operating temperature (150 °C) at an applied heat flux of 10 kW m\(^2\), while actively cooled composites stayed within operating temperature at heat fluxes up to 300 kW m\(^2\).

A promising application for vascular cooling is the preservation of composite mechanical properties in harsh thermal environments. Coppola, White and coworkers recently performed thermomechanical tests to demonstrate this concept. Flexural tests were performed on glass-fiber/epoxy composites subject to heating in a convection oven ([Fig. 1.5a]) [79]. Up to 90% retention of flexural modulus was displayed at an ambient temperature of 325 °C. Next, time-to-failure tests were performed on carbon/epoxy panels subject to 200 MPa compressive load and applied radiative heat fluxes representative of fires ([Fig. 1.5b]) [80]. Actively cooled composites survived for over 30 minutes at 60 kW/m\(^2\) while uncooled composites failed in less than one minute under the same conditions.
1.2.2.2. Computational cooling studies

Computational optimization has been performed to optimize the channel design of microvascular composites. Aragon et al. used genetic algorithms to optimize 2D channel networks for competing objective functions of low pumping pressure, low channel volume fraction, and high flow uniformity [81]. Networks with high redundancy were found best for providing uniform flow and low pressure at reasonable volume fraction. Katifori et al. [54] and Corson et al. [82] optimized channel networks to maintain flow uniformity when subject to blockages and produced similar redundant networks. Aragon et al. later used genetic algorithms to optimize an actively cooled composite for objective functions of low temperature, low pumping pressure, and low channel volume fraction [83,84]. A library of complex networks was created in 2D and 3D to minimize the objective functions under different heating scenarios.

Parametric studies also provide key insight into the design of microvascular composites. Soghrati and coworkers simulated a woven fiberglass composite with straight and sinusoidal channels subject to heat fluxes representative of hypersonic flight [85,86]. Parametric cooling studies in 2D and 3D were performed for different values of channel wavelength, channel amplitude, channel spacing, sample length, applied heat flux, coolant flow rate, and flow direction of adjacent channels (unidirectional vs. counter flow). Composites with straight channels near the heat source, low channel spacing, and high flow rate maintained temperatures below 150 °C at heat fluxes as high as 100 kW/m².
Finally, the literature on traditional aluminum cooling panels provides insight into the design of 2D microchannel networks. Single-channel network designs (e.g. serpentine and spiral networks) and branched network designs (e.g. parallel, bifurcating, etc.) have been studied in detail for fuel cell cooling panels [87,41,88,89]. Spiral designs typically give optimal thermal performance at the expense of higher pumping pressure [87,41]. For branched designs, bifurcating networks typically outperform parallel networks due to better flow distribution [89]. A small number of studies have also been performed on channel design for battery cooling panels. Jarrett et al. optimized spiral channels for a cooling panel subject to the operating conditions of the Chevy Volt [90,91] and developed designs that minimized pumping pressure, maximum panel temperature, and variance in panel temperature.

While the cooling studies presented above demonstrate the utility of actively cooled composites, none have considered the use of composites as cooling panels designed to keep a heat source (e.g. a battery) within a narrow temperature range. Prior studies have focused on composite panels with relatively small planar area (<100 mm x 100 mm) compared to prismatic batteries. Finally, while some studies showed agreement between experiment and simulation, their scope has been restricted to a limited number of test conditions.

1.2.3. Effect of vasculature on mechanical properties

While vasculature enables composites with new functions, the presence of microchannels can potentially decrease mechanical properties. As reviewed by Saeed et al. [92], many studies have been performed on how straight microchannels affect composite mechanical properties such as tensile strength/stiffness, compressive strength/stiffness, fatigue behavior, mode 1 and mode 2 fracture toughness, low-velocity impact response, interlaminar shear strength, and bending strength/stiffness. Studies have investigated channels with different diameter, spacing, shape (circular vs. elliptical), orientation with respect to load direction, and orientation with respect to surrounding fibers. Two main channel incorporation schemes have been used: placing a hollow or sacrificial fiber directly between plies (deemed scheme A) or placing a hollow or sacrificial fiber between pre-cut plies (deemed scheme B). These mechanical studies are reviewed below. In general, mechanical properties are preserved in a composite when channel diameter is less than ca. 600 µm, channel volume fraction is low (< 2%), and
channel incorporation does not disrupt the angle or continuity of load-bearing plies. Notably, no studies have been performed to date on how channels affect composite energy absorption in crash.

Tensile and compressive strength/stiffness have been studied in detail for microvascular composites. Kousourakis et al. tested the tensile and compressive properties of carbon/epoxy composites with 170 - 680 µm diameter microchannels at 5 mm spacing [65]. Longitudinal channels (channels aligned with the load path) caused no significant knockdown in stiffness or strength, but transverse channels caused knockdowns of up to 20%. These knockdowns were attributed to ply waviness caused by the scheme A fabrication scheme, leading to premature failure at the channels as observed in experiments (Fig. 1.6a) and finite element simulations (Fig. 1.6b) [93]. Hartl et al. simulated tensile, compressive, and biaxial loading of carbon/epoxy composites with 300 µm microchannels at close spacing (1 or 3 mm) [94]. Significant knockdowns of strength and stiffness (up to 40%) were reported when channels caused substantial ply waviness (for scheme A) or when channels replaced a significant volume fraction of load-bearing plies (for scheme B). To incorporate channels more seamlessly into a composite, Coppola et al. wove 500 µm PLA fibers into a 3D fiberglass preform and formed channels using VaSC [95]. The resulting composites had no significant knockdowns in tensile strength or stiffness regardless of channel orientation.

![Image](image_url)

**Fig. 1.6.** Testing and simulation of tensile failure of microvascular composites. a) Post-fracture image of a carbon/epoxy composite sample containing 580 µm transverse channels (image reproduced from [65] with permission from Elsevier). b) Simulated fiber fracture damage index of the same sample before failure (image reproduced from [93] with permission from Elsevier).

Fracture toughness has actually shown to increase with the addition of microchannels. Norris et al. tested the mode 1 and mode 2 fracture toughness of carbon/epoxy composites with 250 µm and 500 µm
channels [63]. No change in fracture toughness was observed for longitudinal channels (channels aligned with crack propagation) or for transverse channels incorporated with scheme B. Transverse channels incorporated with scheme A caused an increase in fracture toughness of over 100%, which was attributed to crack blunting and deflection. Similar increases in fracture toughness were reported experimentally by Kousourakis et al. [96] and Phillips et al. [77] and computationally by Zhao et al. [97].

Impact response, fatigue behavior, flexural properties, and interlaminar shear strength (ILSS) have also been studied for microvascular composites. Norris et al. tested the compression after impact (CAI) response of carbon/epoxy composites with 250 µm and 500 µm channels [98]. Knockdowns in compressive properties before and after impact were observed for cases of substantial ply waviness (for scheme A) and significant removal of load-bearing plies (for scheme B). To investigate fatigue, Kousourakis et al. tested the tensile and compressive strength reduction of carbon/epoxy composites subject to 1 million load cycles and found that 680 µm channels had no significant effect on behavior [99]. Flexural properties are generally preserved in microvascular composites, since channels are usually placed in the midplane of a composite and thus away from primary flexural load paths [79,100,101]. In contrast, ILSS has shown to decrease for microvascular composites due to the load path being concentrated at the sample midplane [96].

1.3. Overview and outline of dissertation

This dissertation examines the use of microvascular composites as a structural, thermally responsive material for EVs. The content is organized as follows. In Chapter 2, carbon/epoxy panels with straight microchannels are investigated for battery cooling. Microvascular composites are fabricated using vacuum assisted resin transfer molding (VARTM), with channels formed using either removable nylon fibers or the VaSC technique. Thermography is used to assess panel cooling performance over a range of conditions representative of EV battery cooling. Computational fluid dynamics (CFD) simulations are validated by experiment across a wide range of conditions and then used to more accurately simulate how panels would perform in a battery pack. The results confirm that panels with sufficient channel density and coolant flow rate can sufficiently cool EV batteries below 40 °C for applied heat fluxes as high as 1000 W m⁻².
Chapter 3 reports the development of carbon/epoxy cooling panels with more advanced 2D cooling networks. Parallel, bifurcating, serpentine, and spiral networks are formed using the vaporization of laser-cut PLA templates. Experiments and simulations show that the spiral network provides the most uniform temperature distribution at high pumping pressure (>100 kPa) while the bifurcating network offer slightly reduced thermal performance at much lower pressure (<10 kPa). Channel size is shown to have little effect on cooling performance. Tests are then performed on a parallel network design optimized to reduce panel temperature in a collaborative study [102]. The design obtained from gradient-based optimization outperforms the reference design as predicted.

In Chapter 4, another collaborative study is performed to optimize grid-based channel networks for blockage tolerance. Microvascular silicone panels are simulated with a locally applied heat flux of 2000 W m\(^{-2}\). Silicone panels were chosen due to their low cost and high fabrication speed. Networks with a nodal degree of 2 – 6 (a measure of redundancy) are optimized to minimize panel temperature while the network is subject to a single blockage. The results demonstrate that optimized networks with high nodal degree resist temperature spikes when blocked. For instance, a blockage in the 6-degree optimized network causes a temperature rise of 7 °C compared to 35 °C for a 2-degree network optimized without considering blockages. Simulations are validated with microvascular silicone panels fabricated using a novel laser cutting technique.

While Chapters 2, 3, and 4 report on cooling performance, Chapter 5 reports on the energy absorption of microvascular carbon/epoxy panels in a crash. First, corrugated carbon/epoxy panels are fabricated with 400 um straight channels at different spacing (10 mm and 1.2 mm) and orientation with respect to load (longitudinal vs. transverse) and to surrounding plies (aligned vs. misaligned). Panels are given a chamfer damage trigger and compressed to measure their specific energy absorbed (SEA). Channels caused no significant knockdown in SEA, regardless of spacing or orientation. Finally, microchannels are shown to be capable of triggering stable, energy-absorbing damage modes during compression in lieu of a chamfer. Flat (non-corrugated) panels are fabricated with one, three or five channels located 2 mm from the bottom of the panel. Vascular panels fail stably via crack initiation at the channels, while non-vascular panels fail catastrophically and absorb ca. 10 times less energy.

Chapter 6 gives conclusions from this work and presents several directions for future studies.
CHAPTER 2
COMPOSITES WITH STRAIGHT CHANNELS FOR BATTERY PACKAGING

2.1. Introduction

Batteries in electric vehicles (EVs) require packaging that provides both thermal regulation and crash protection. A novel packaging scheme is presented that uses active cooling of microvascular carbon fiber reinforced composites to accomplish this multifunctional objective. Batteries are embedded within microvascular carbon fiber/epoxy composites that contain straight channels for coolant flow (Fig. 2.1a). Straight (1D) microchannels are chosen due to their ease of fabrication. Batteries can be stacked into a single pack (Fig. 2.1b) or incorporated into composite body panels in the vehicle. Several studies have confirmed that straight channels provide composites with efficient thermal management as reviewed in Chapter 1 [79,80,86]. However, this study serves as the first to demonstrate that composites can act as full-scale cooling panels designed to keep a heat source in a narrow temperature window.

Fig. 2.1. Schematic of battery packaging using microvascular composite panels. a) Battery cell is sandwiched between two microvascular composite panels through which coolant flows. b) Assembly of multiple battery cells with a coolant distribution manifold.

* This chapter is adapted from an article submitted to Smart Materials and Structures [106].
Microvascular composites are fabricated by vacuum-assisted resin transfer molding (VARTM) and the use of sacrificial fibers. Cooling performance is characterized across a range of coolant flow rates, applied heat flux, channel spacings, and carbon fiber materials. Computational fluid dynamics (CFD) simulations are also performed and are validated across all experimental test conditions. Finally, further simulations are performed to design vascular cooling panels for use in a typical EV battery pack. Simulations are performed using different panel materials, channel spacings, applied heat fluxes, coolant flow rates, and panel thicknesses.

2.2. Materials and methods

2.2.1. Fabrication and testing of microvascular composite panels

2.2.1.1. Fabrication of microvascular composite panels

Microvascular composite panels were prepared using vacuum-assisted resin transfer molding (VARTM). Panels were made with both polyacrylonitrile (PAN) and pitch derived carbon fiber fabrics. The PAN-based fabric used was a 2 x 2 twill weave of Toray T300 3K fiber (199 g m\(^{-2}\), Soller Composites) and the pitch-based fabric was a plain weave of ThermalGraph EWC-300X 4K fiber (610 g m\(^{-2}\), Cytec). Fabrics were infused with an epoxy resin composed of 100:35 Araldite LY 8605 epoxide to Aradur 8605 curing agent (Huntsman Advanced Materials, LLC).

For PAN-based carbon/epoxy panels, 12 layers of carbon fiber fabric were stacked in a double vacuum bag layup with 500 µm diameter sacrificial fibers placed between the 6th and 7th layers of fabric (Fig. 2.2). Channels were evenly spaced throughout the panel (see Table 2.1 for channel spacings for different panels). For all panels but one, the sacrificial fibers were nylon monofilament (Berkley) treated with Henkel FREKOTE release agent (Northern Composites) and manually extracted after cure. As a proof of concept, in one panel polylactide (PLA) fibers treated with tin (II) oxalate catalyst were used as sacrificial fibers and removed using the VaSC technique [67].
Double-bag vacuum-assisted resin transfer molding (VARTM) layup for fabricating microvascular composites. Sacrificial fibers are placed between fabric layers and removed/evacuated after cure.

Table 2.1. Channel spacings used in composite panels.

<table>
<thead>
<tr>
<th>Number of Channels</th>
<th>Spacing between channels (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>3</td>
<td>72.5</td>
</tr>
<tr>
<td>4</td>
<td>53.2</td>
</tr>
<tr>
<td>6</td>
<td>34.7</td>
</tr>
<tr>
<td>8</td>
<td>25.8</td>
</tr>
<tr>
<td>12</td>
<td>17.0</td>
</tr>
<tr>
<td>16</td>
<td>12.7</td>
</tr>
</tbody>
</table>

The sacrificial PLA fibers were prepared using a modified solvent-impregnation procedure adapted from Esser-Kahn et al. [67] and Dong et al. [68]. Commercial 500 µm polylactide (PLA) monofilament (Nextrusion Inc.) was wound on a reel and immersed in a narrow neck beaker containing 480 mL trifluoroethanol (TFE, Halogen Inc.), 320 mL deionized water, 13 g SnOx (Sigma), 0.5 g rhodamine 6G (Sigma) and 40 ml Disperbyk 187 (Byk Chemie). The beaker was suspended in a 37 °C water bath and stirred with a digital mixer (Eurostar, IKA Labortechnik) at 400 RPM for 24 hr. The beaker was capped to prevent evaporation of solvent. After 24 hr, the reel was removed and dried overnight in a 35 °C oven.

Prior to infusion, epoxy resin was mixed by hand and degassed for 3 hr at room temperature. Resin was infused into the fabric layup using a vacuum pump at ~38 torr. The resin was cured at room temperature under vacuum for 24 hr and then 121 °C for 2 hr followed by 177 °C for 3 hr. The heating
rate was 2 °C min\(^{-1}\) and the cooling rate to ambient was 1 °C min\(^{-1}\). The average thickness of the PAN-based carbon/epoxy panels was 2.71 ± 0.07 mm which corresponds to a fiber volume fraction of 49.5 ± 1.3% based on ASTM D3171 (Test Method II).

After cure, the sacrificial nylon fibers were manually extracted using needle-nose pliers. This technique produced high-fidelity circular channels with little disruption to fiber architecture (Fig. 2.3). PLA fibers were evacuated through the VaSC process (200 °C and vacuum for 48 hr) and gave similar quality channels. The VaSC process requires longer manufacturing time, but it is more scalable and can create channels with much more complex shapes [67,69].

One pitch-based carbon/epoxy panel was made using 4 layers of carbon fiber fabric in a double vacuum bag layup with sacrificial fibers placed between the 2\(^{nd}\) and 3\(^{rd}\) layers of fabric. The pitch-based carbon/epoxy panel had a thickness of 3.03 mm which corresponds to a fiber volume fraction of 38.3%.

![Fig. 2.3. Microvascular composite panel. a) 150 mm x 200 mm carbon fiber/epoxy panel with 6 cooling channels (500 µm diameter) with inlet and outlet needles attached. b) Cross-sectional optical micrographs of a 500 µm diameter cooling channel embedded in carbon/epoxy composite.](image)

2.2.1.2. Cooling experiments

Vascular panels were cut to 150 mm x 200 mm with a sectioning saw (Buehler IsoMet 1000) and the top surface was painted with a matte black paint (Krylon) for thermal imaging. Inlet and outlet holes 1.03
mm in diameter and 3 mm deep were drilled into each channel to allow the insertion of needle fittings. Cooling tests were performed using the experimental setup shown in Fig. 2.4. Panels were placed on top of a copper plate coupled to a polyimide flexible heater (Omega, part # KH-608/2) with thermal grease (Omega, part # Omegatherm 201). Applied heat flux was controlled by regulating the voltage supplied to the heater (see Section S2) with a Variac variable transformer (Staco Energy Products Co., Type L1010). Voltage values were correlated to heat flux values using the equation $\dot{q}'' = \frac{V^2}{RA}$ where $\dot{q}''$ is areal heat flux, $V$ is voltage, $R$ is heater resistance, and $A$ is heater area. The heater assembly was placed on a balsa wood platform while the sides were insulated using fiberglass insulation.

![Fig. 2.4. Schematic of experimental setup where panel temperature is measured with an IR camera while coolant temperature is monitored with thermocouples embedded in the microchannel inlets and outlets.](image)

The coolant was a 50:50 water/ethylene glycol mixture (Macron Chemicals) and was stored in a circulator (Julabo, Model F32-HP) at 21 °C and pumped through the composite with a peristaltic pump (Cole-Parmer Masterflex, Model EW-07551099). Coolant flow was evenly divided between channels. Tubing lines were connected to the composite with 19 gauge needle fittings. The fittings contained Type T thermocouple probes (Omega, part # TMQSS-020U-36) to measure coolant temperature, which were inserted by drilling a hole into the fittings and sealing the thermocouple with acrylate adhesive (Loctite Depend 330). For 3, 4, and 6-channel panels, thermocouples were placed at all channel inlets and outlets. For 8 and 12-channel panels, thermocouples were placed at every other inlet and outlet. Thermocouple measurements were taken using 4-port thermocouple readers (Phidgets Inc., model 1048).
controlled using LabVIEW 2013. Surface temperature measurements of the panel were taken with an infrared (IR) camera (FLIR, Model SC620).

Experiments were performed to measure the surface temperature and the average coolant temperature rise ($\Delta T_c$) at steady-state. The cooling efficiency ($\eta$) of the panel was then calculated as the ratio of the heat flux absorbed by the channels ($q_c$) to the total heat flux applied ($q_t$)

$$\eta = \frac{q_c}{q_t} = \frac{\dot{m}c_p\Delta T_c}{q_t}$$ (2.1)

where $\dot{m}$ is coolant flow rate and $c_p$ is the specific heat of the coolant.

2.2.2. Computational fluid dynamics simulations

2.2.2.1. Model conditions

The cooling performance of microvascular panels was analyzed using the commercial computational fluid dynamics (CFD) package ANSYS Fluent v15.0. The model simulated is shown in Fig. 2.5 and consists of a vascular composite heated from one side (copper plate/heater) and open to convection and radiation on the opposite side. The side walls are insulated in the plane and coolant is circulated with a constant inlet temperature (21.5 °C) based on the average inlet temperature read by thermocouples during experiments. The full set of dimensions, boundary conditions, and material properties used for all simulations is included in Table 2.2.
Fig. 2.5. Schematic of simulation setup from the top and side views (dimensions in mm). The microvascular composite panel is heated from below while coolant circulates through all channels at a set inlet temperature and flow rate.

A surface convection coefficient of $h = 8 \text{ W m}^{-2} \text{K}^{-1}$ was chosen after fitting to experimental data. The emissivity of the top surface was set to 0.97 to represent the matte black painted surface. Coolant material properties were taken from literature [90] and composite properties were calculated based on constituent properties and fiber volume fraction (see constituent properties in Table 2.3).

A set of simulations was also performed to analyze cooling performance under conditions more realistic for in situ battery packaging. In this case, panels are heated from both top and bottom surfaces (see Fig. 2.1b), there is no copper plate between the panels and the batteries, and the coolant inlet temperature would likely be higher than room temperature [90]. Commercial microvascular panels would also maximize fiber volume fraction and thus, thermal conductivity (see Table 2.2). Simulations were also performed for aluminum panels as a comparison case.
Table 2.2. Dimensions, boundary conditions, and material properties used for cooling simulations.

<table>
<thead>
<tr>
<th>Simulation Condition or Material Property</th>
<th>Value for experimental validation</th>
<th>Value for in situ battery cooling</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Plate dimensions</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Plate width (mm)</td>
<td>150</td>
<td>160</td>
<td></td>
</tr>
<tr>
<td>Plate height (mm)</td>
<td>200</td>
<td>200</td>
<td></td>
</tr>
<tr>
<td>Plate thickness (mm)</td>
<td>2.7&lt;sup&gt;a&lt;/sup&gt;</td>
<td>3&lt;sup&gt;b&lt;/sup&gt;</td>
<td></td>
</tr>
<tr>
<td>Channel diameter (mm)</td>
<td>0.5</td>
<td>0.5</td>
<td></td>
</tr>
<tr>
<td>Channel location through-thickness</td>
<td>Centered</td>
<td>Centered</td>
<td></td>
</tr>
<tr>
<td>Channel spacing</td>
<td>See Table S1</td>
<td>See Table S1</td>
<td></td>
</tr>
<tr>
<td><strong>Boundary conditions</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Applied heat flux (W m&lt;sup&gt;-2&lt;/sup&gt;)</td>
<td>500 from back&lt;sup&gt;b&lt;/sup&gt;</td>
<td>250 from front and back&lt;sup&gt;b&lt;/sup&gt;</td>
<td></td>
</tr>
<tr>
<td>Total coolant flow rate (mL min&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>28.2&lt;sup&gt;b&lt;/sup&gt;</td>
<td>28.2&lt;sup&gt;b&lt;/sup&gt;</td>
<td></td>
</tr>
<tr>
<td>Coolant inlet temperature (°C)</td>
<td>21.5</td>
<td>27</td>
<td></td>
</tr>
<tr>
<td>Coolant outlet pressure (Pa)</td>
<td>0</td>
<td>0</td>
<td></td>
</tr>
<tr>
<td>Thickness of copper plate between heater and composite (mm)</td>
<td>1.6</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>Convection coefficient h for top face (W m&lt;sup&gt;-2&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>8</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>Emissivity for top face</td>
<td>0.97</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>Air temperature surrounding top face (°C)</td>
<td>21</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>Sides of panel</td>
<td>Insulated</td>
<td>Insulated</td>
<td></td>
</tr>
<tr>
<td><strong>Coolant: 50/50 water/ethylene glycol</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Density (kg m&lt;sup&gt;-3&lt;/sup&gt;)</td>
<td>1065</td>
<td>1065</td>
<td>[90]</td>
</tr>
<tr>
<td>Viscosity (Pa s)</td>
<td>0.0069* (T/273)</td>
<td>0.0069* (T/273)</td>
<td>[90]</td>
</tr>
<tr>
<td>Specific heat (J kg&lt;sup&gt;-1&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>2574.7 + 3.0655*T</td>
<td>2574.7 + 3.0655*T</td>
<td>[90]</td>
</tr>
<tr>
<td>Thermal conductivity (W m&lt;sup&gt;-1&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>0.419</td>
<td>0.419</td>
<td>[90]</td>
</tr>
<tr>
<td><strong>PAN-based carbon fiber composite</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fiber volume fraction V&lt;sub&gt;f&lt;/sub&gt; (%)</td>
<td>49.5</td>
<td>60</td>
<td>ASTM D3171 for experimental value</td>
</tr>
<tr>
<td>Density (kg m&lt;sup&gt;-3&lt;/sup&gt;)</td>
<td>1440</td>
<td>1500</td>
<td>&lt;sup&gt;c&lt;/sup&gt;</td>
</tr>
<tr>
<td>Specific heat (J kg&lt;sup&gt;-1&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>880</td>
<td>860</td>
<td>&lt;sup&gt;c&lt;/sup&gt;</td>
</tr>
<tr>
<td>In-plane thermal conductivity (W m&lt;sup&gt;-1&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>2.2</td>
<td>2.7</td>
<td>&lt;sup&gt;c&lt;/sup&gt;</td>
</tr>
<tr>
<td>Transverse thermal conductivity (W m&lt;sup&gt;-1&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>0.47</td>
<td>0.59</td>
<td>&lt;sup&gt;c&lt;/sup&gt;</td>
</tr>
<tr>
<td><strong>Pitch-based carbon fiber composite</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fiber volume fraction (%)</td>
<td>38.3</td>
<td>60</td>
<td>ASTM D3171 for experimental value</td>
</tr>
<tr>
<td>Density (kg m&lt;sup&gt;-3&lt;/sup&gt;)</td>
<td>1490</td>
<td>1710</td>
<td>&lt;sup&gt;c&lt;/sup&gt;</td>
</tr>
<tr>
<td>Specific heat (J kg&lt;sup&gt;-1&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>850</td>
<td>790</td>
<td>&lt;sup&gt;c&lt;/sup&gt;</td>
</tr>
<tr>
<td>In-plane thermal conductivity (W m&lt;sup&gt;-1&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>48</td>
<td>75</td>
<td>&lt;sup&gt;c&lt;/sup&gt;</td>
</tr>
<tr>
<td>Transverse thermal conductivity (W m&lt;sup&gt;-1&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>0.39</td>
<td>0.61</td>
<td>&lt;sup&gt;c&lt;/sup&gt;</td>
</tr>
<tr>
<td><strong>Aluminum</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Density (kg m&lt;sup&gt;-3&lt;/sup&gt;)</td>
<td>N/A</td>
<td>2720</td>
<td>ANSYS Fluent 15.0</td>
</tr>
<tr>
<td>Specific heat (J kg&lt;sup&gt;-1&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>N/A</td>
<td>870</td>
<td>ANSYS Fluent 15.0</td>
</tr>
<tr>
<td>Thermal conductivity (W m&lt;sup&gt;-1&lt;/sup&gt; K&lt;sup&gt;-1&lt;/sup&gt;)</td>
<td>N/A</td>
<td>202</td>
<td>ANSYS Fluent 15.0</td>
</tr>
</tbody>
</table>

<sup>a</sup> Value of 3 mm used for pitch-based carbon fiber composite.

<sup>b</sup> Baseline value. Other values were also tested.

<sup>c</sup> Determined using constituent properties and fiber volume fraction. Epoxy properties and fiber properties are given in Table 2.3. Density and specific heat were determined by rule of mixtures. Conductivity was determined by rule of mixtures for axial conductivity and using the self-consistent model for transverse conductivity (see [79] for calculations).
Table 2.3. Epoxy and carbon fiber material properties.

<table>
<thead>
<tr>
<th>Material Property</th>
<th>Value</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>Epoxide – Araldite LY/Aradur 8605</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Density (kg m(^{-3}))</td>
<td>1120</td>
<td>Measured with ASTM D792</td>
</tr>
<tr>
<td>Specific heat (J kg(^{-1}) K(^{-1}))</td>
<td>1000</td>
<td>Manufacturer value</td>
</tr>
<tr>
<td>Thermal conductivity (W m(^{-1}) K(^{-1}))</td>
<td>0.2</td>
<td>Manufacturer value</td>
</tr>
<tr>
<td>PAN-based carbon fiber – Toray T300</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Density (kg m(^{-3}))</td>
<td>1760</td>
<td>Manufacturer value</td>
</tr>
<tr>
<td>Specific heat (J kg(^{-1}) K(^{-1}))</td>
<td>800</td>
<td>Manufacturer value</td>
</tr>
<tr>
<td>Axial thermal conductivity (W m(^{-1}) K(^{-1}))</td>
<td>7.9</td>
<td>[103]</td>
</tr>
<tr>
<td>Transverse thermal conductivity (W m(^{-1}) K(^{-1}))</td>
<td>2.0</td>
<td>[104]</td>
</tr>
<tr>
<td>Pitch-based carbon fiber – Cytec ThermalGraph EWC-300X</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Density (kg m(^{-3}))</td>
<td>2100</td>
<td>Manufacturer value</td>
</tr>
<tr>
<td>Specific heat (J kg(^{-1}) K(^{-1}))</td>
<td>710</td>
<td>[105]</td>
</tr>
<tr>
<td>Axial thermal conductivity (W m(^{-1}) K(^{-1}))</td>
<td>250</td>
<td>Manufacturer value</td>
</tr>
<tr>
<td>Transverse thermal conductivity (W m(^{-1}) K(^{-1}))</td>
<td>2.4</td>
<td>[104]</td>
</tr>
</tbody>
</table>

2.2.2.2. Mesh generation and convergence

Conforming, hexahedral finite volume meshes of panels were constructed in ANSYS Meshing v14.5. A mesh convergence study was performed on a panel 3 mm thick with 6 embedded microchannels. Fig. 2.6 shows a convergence plot of average surface temperature for the panel vs. the number of fluid elements in the mesh. Based on this study, a maximum fluid mesh sizing of 40 µm was defined yielding 350,000 fluid elements and 750,000 total elements for the 6-channel panel. The same mesh sizings were used for all other panels, yielding a range of 410,000 – 4,000,000 total elements for all panels simulated.

Simulations were performed in ANSYS Fluent using the SIMPLE pressure-velocity coupling scheme, Green-Gauss node-based gradient discretization, second-order pressure discretization, third-order MUSCL momentum discretization, and third-order MUSCL energy discretization. The simulations solve for the conservation of mass, momentum, and energy across a finite volume mesh for a Newtonian, incompressible fluid with laminar flow. The maximum Reynolds number for any simulation was ~200, confirming the assumption of laminar flow. Simulations were performed to obtain the temperature profile of the panels at steady-state. The convergence criterion employed was for velocity and continuity.
residuals to reach $10^{-4}$ and for the energy residual to reach $10^{-9}$. These thresholds were shown to be sufficient for full convergence of panel and coolant temperature fields.

Fig. 2.6. Mesh convergence plot of average steady-state surface temperature for a 3 mm thick 6-channel PAN-based carbon fiber/epoxy panel subject to the baseline *in situ* battery cooling conditions (500 W m$^{-2}$ applied heat flux and 28.2 mL min$^{-1}$ total coolant flow rate).

2.3. Results and discussion

2.3.1. Experimental characterization of cooling performance

2.3.1.1. Effect of coolant flow rate

A parametric study was performed to characterize cooling panels across a wide range of operating conditions. The baseline test case consists of a 500 W m$^{-2}$ applied heat flux and 28.2 mL min$^{-1}$ (0.5 g s$^{-1}$) total flow rate applied to a 6-channel PAN-based carbon/epoxy vascular panel. The heater size and baseline heat flux are representative of the thermal load of one battery cell in the Chevrolet Volt [90,91]. These tests represent a 1:1 battery cell: cooling panel ratio as shown for the stacking sequence in Fig. 2.1b. The choice of coolant and baseline flow rate are also inspired by the Chevrolet Volt system [90,91].

The effect of total flow rate on the cooling performance of a 6-channel PAN-based carbon/epoxy panel is shown in Fig. 2.7. Three panels were tested for repeatability: two panels in which channels were made with nylon monofilament and one panel where the channels were made with the VaSC technique. No significant difference was seen between panels made with the different fabrication techniques. For the
case where no flow occurs, the average panel surface temperature is ~52 °C and all heat is lost through radiation and convection to the environment. As the flow rate is increased, heat is absorbed by the coolant and transported through the channels. Average panel temperature quickly decreases until reaching a plateau value at high flow rate, as does the maximum surface temperature (see summary of all experimental data in Table 2.4). Surface temperature profiles reveal warm regions between channels and a slight increase in temperature in the direction of coolant flow.

Cooling efficiency ($\eta$) quickly increases with increasing flow rate (Fig. 2.7) until reaching about 80% at high flow rates. Flow rates above 30 – 40 mL min$^{-1}$ are observed to provide minimal additional cooling capability while requiring higher pump pressure and power. Good agreement was obtained between experimental data and simulation results for surface temperature and cooling efficiency across all conditions tested. Small discrepancies between experimental and simulated values are likely experimental error and/or variation in convection coefficient (see discussion in Section 2.3.1.5).
Table 2.4. Comparison of average steady-state surface temperature $T_{av}$, maximum surface temperature $T_{max}$, and coolant temperature rise $\Delta T_c$ for experiments and simulations. Experimental error refers to the largest deviation of any sample from the mean value. The baseline test conditions are in bold.

<table>
<thead>
<tr>
<th>Sample</th>
<th>Samples tested</th>
<th>Heat flux $(W \ m^{-2})$</th>
<th>Total flow rate $(mL \ min^{-1})$</th>
<th>$T_{av}$ $(^\circ C)$</th>
<th>$T_{max}$ $(^\circ C)$</th>
<th>$\Delta T_c$ $(^\circ C)$</th>
</tr>
</thead>
<tbody>
<tr>
<td>6-channel PAN</td>
<td>3</td>
<td>500</td>
<td>0</td>
<td>55.1</td>
<td>51.5 ± 0.8</td>
<td>55.1</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>7.1</td>
<td>38.1</td>
<td>39.1 ± 0.1</td>
<td>39.5</td>
<td>40.8 ± 0.2</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>14.1</td>
<td>34.6</td>
<td>35.5 ± 0.4</td>
<td>35.0</td>
<td>37.2 ± 0.6</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>28.2</td>
<td>31.0</td>
<td>32.0 ± 0.5</td>
<td>32.3</td>
<td>33.6 ± 0.5</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>42.3</td>
<td>30.0</td>
<td>31.6 ± 0.7</td>
<td>31.3</td>
<td>33.2 ± 0.8</td>
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<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>56.4</td>
<td>29.5</td>
<td>31.2 ± 0.4</td>
<td>30.7</td>
<td>32.8 ± 0.4</td>
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<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>70.5</td>
<td>29.2</td>
<td>30.9 ± 0.5</td>
<td>30.4</td>
<td>32.4 ± 0.5</td>
</tr>
<tr>
<td>6-channel PAN</td>
<td>3</td>
<td>250</td>
<td>28.2</td>
<td>26.4</td>
<td>26.7</td>
<td>26.8</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>750</td>
<td>36.9</td>
<td>37.4</td>
<td>37.7</td>
<td>39.7 ± 0.6</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>1000</td>
<td>42.2</td>
<td>42.9</td>
<td>43.0</td>
<td>45.9 ± 0.8</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>1250</td>
<td>48.8</td>
<td>47.5</td>
<td>48.3</td>
<td>51.1 ± 0.8</td>
</tr>
<tr>
<td>3-channel PAN</td>
<td>1</td>
<td>500</td>
<td>28.2</td>
<td>35.1</td>
<td>36.7</td>
<td>36.2</td>
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<td>4-channel PAN</td>
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<td>&quot;</td>
<td>33.2</td>
<td>34.8</td>
<td>34.4</td>
<td>34.6</td>
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<tr>
<td>8-channel PAN</td>
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<td>&quot;</td>
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<td>31.0</td>
<td>31.0</td>
<td>32.7</td>
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<td>28.3</td>
<td>30.1</td>
<td>29.6</td>
<td>31.5</td>
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<td>3-channel Pitch</td>
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<td>500</td>
<td>&quot;</td>
<td>30.0</td>
<td>32.5</td>
<td>31.2</td>
</tr>
</tbody>
</table>

2.3.1.2. Effect of heat flux

The effect of applied heat flux on cooling performance for a 6-channel PAN-based carbon/epoxy panel is presented in Fig. 2.8. Higher heat fluxes represent higher power density batteries and more rapid charge/discharge conditions. Average and maximum surface temperature (and $\Delta T_c$) rise linearly with heat flux as expected. Cooling efficiency is roughly constant over the range of heat fluxes tested.
Fig. 2.8. Thermal performance of a 6-channel PAN-based carbon/epoxy panel subject to 28.2 mL min$^{-1}$ total coolant flow rate as a function of applied heat flux. a) Plot of average steady-state surface temperature and cooling efficiency ($\eta$) vs. applied heat flux. Error bars represent the maximum and minimum values for 3 replicate panels. b) Representative surface temperature profiles for experiment and simulation at different applied heat fluxes.

2.3.1.3. Effect of channel spacing

Composite panels with 3, 4, 6, 8, and 12 channels were tested to study the effect of channel spacing on cooling performance. The results of this study (Fig. 2.9) reveal that average surface temperature decreases and cooling efficiency increases with increasing number of channels. The better cooling performance is due to both an increase in channel surface area, which results in lower radial thermal gradients within channels [45], and by the reduction in distance between channels (and the resulting reduction in heat conduction distance to channels).
2.3.1.4. Effect of fiber conductivity

While PAN-based carbon fibers are the most common and economical class of carbon fiber, pitch-based carbon fibers offer substantially higher axial thermal conductivity (~250 W m\(^{-1}\) K\(^{-1}\) for pitch-based carbon fiber vs. ~8 W m\(^{-1}\) K\(^{-1}\) for PAN-based carbon fiber). A 3-channel pitch-based carbon/epoxy panel was fabricated and tested to compare to an equivalent PAN-based carbon/epoxy panel. The pitch-based panel produces a much more uniform temperature distribution compared to the PAN-based panel (Fig. 2.10). The higher in-plane thermal conductivity reduces in-plane thermal gradients, lowering the average surface temperature and increasing cooling efficiency. Pitch-based carbon fiber composites offer superior cooling performance compared to PAN-based carbon fiber composites, albeit at higher material cost.
Fig. 2.10. Comparison of 3-channel cooling performance for a PAN-based carbon/epoxy panel vs. a pitch-based carbon/epoxy panel. Steady-state surface temperature profiles are shown for 500 W m\(^{-2}\) applied heat flux and 28.2 mL min\(^{-1}\) total coolant flow rate. Cooling efficiency (\(\eta\)) is denoted for each case.

2.3.1.5. Discrepancies between experimental data and simulated values

For all experiments, the maximum discrepancy between experiment and simulation for average or maximum surface temperature was 3.6 °C (Table 2.4). The IR camera has a stated accuracy of ± 2 °C, and the remaining discrepancy is most likely associated with variation in the convection coefficient (\(h\)). A single value of \(h = 8\) W m\(^{-2}\) K\(^{-1}\) was chosen for all simulations, but \(h\) likely varies for different operating conditions. The next most likely sources of discrepancy is thermal conductivity of the composite, since it was calculated based on published constituent material properties and acceptable material models.

The maximum discrepancy between experimental data and simulated results for coolant temperature rise \(\Delta T_c\) was 1.7 °C (Table 2.4). The thermocouples have a listed accuracy of ± 0.5 °C, so subtracting inlet temperatures from outlet temperatures gives an accuracy of ± 1.0 °C. The remaining discrepancy could again be attributed to the selected value of \(h\) and to composite thermal conductivity values.
2.3.2. Simulations of in situ battery cooling

2.3.2.1. Effect of panel material and channel spacing

The Fluent model was validated by experiments across a wide range of operating conditions. Further simulations were also performed that better represent in situ battery pack cooling conditions. These simulations consist of a vascular carbon/epoxy composite panel being heated from both top and bottom surfaces. Composite panels were assumed to have a higher fiber volume fraction of 60% and the coolant inlet temperature was increased to 27 °C. The cooling performance objective for these simulations is to maintain a steady-state temperature below 40 °C.

Three panel materials with different thermal conductivities were simulated: PAN-based carbon/epoxy \((k \approx 3 \text{ W m}^{-1}\text{K}^{-1} \text{in-plane})\), pitch-based carbon/epoxy \((k \approx 75 \text{ W m}^{-1}\text{K}^{-1})\), and aluminum \((k \approx 200 \text{ W m}^{-1}\text{K}^{-1})\) as a reference case. Panels with these materials and different numbers of channels were simulated for a total applied heat flux of 500 W m\(^{-2}\) and a total coolant flow rate of 28.2 mL min\(^{-1}\) (Fig. 2.11). Aluminum panels performed well and satisfied the target temperature requirement in all cases. Pitch-based carbon/epoxy panels also performed well and met the target temperature requirement for all cases greater than 4 cooling channels.

![Fig. 2.11](image.png)

**Fig. 2.11.** Simulation results for in situ battery cooling using 3 mm thick microvascular panels with different materials and channel spacing. a) Plot of maximum steady-state surface temperature vs. number of channels for panels made with PAN-based carbon/epoxy, pitch-based carbon/epoxy, and aluminum. A heat flux of 250 W m\(^{-2}\) is applied from both sides and the total coolant flow rate is 28.2 mL min\(^{-1}\). b) Surface temperature contours for various panels.
PAN-based carbon/epoxy panels with a small number of cooling channels reach temperatures far above the target value due to hot spots forming between channels (Fig. 2.11). As the number of cooling channels increases, the temperature distribution becomes more uniform and the maximum temperature drops significantly. The 16-channel PAN-based carbon/epoxy panel has a maximum temperature below the target value and a temperature profile similar to panels made with pitch-based carbon/epoxy and aluminum. This channel density corresponds to a channel spacing of 13 mm and a total channel volume fraction of only 0.5%. Significantly, this relatively small volume concentration of 500 µm channels is not expected to affect the mechanical properties of typical woven composites [79,95].

Note that the high panel temperatures for low channel number in Fig. 2.11 are likely overestimated, since these simulations assume the composite panels have perfectly insulated side walls. In reality, the side walls would not be perfectly insulated so higher panel temperatures would lead to increased heat loss through the sides. Simulation accuracy could be increased by simulating side walls with a set thickness, thermal conductivity, and external ambient temperature. For instance, the panels in thermal experiments were insulated with 25 mm thick fiberglass insulation ($k \approx 0.03$ W m$^{-1}$ K$^{-1}$).

2.3.2.2. Effect of coolant flow rate and heat flux

The performance of a 16-channel PAN-based carbon/epoxy panel was analyzed at different applied heat fluxes and flow rates (Fig. 2.12a). For a given heat flux, surface temperature drops quickly with increasing flow rate at first and then begins to plateau. For a given flow rate, increasing heat flux gives linearly increasing surface temperatures, as expected. Interestingly, the panel can cool to the temperature target even at double the baseline heat flux (1000 W m$^{-2}$) for high enough flow rate (>80 mL min$^{-1}$).
2.3.2.3. Effect of panel thickness

Simulations were also performed on 16-channel panels with thicknesses ranging from 1 – 5 mm. Channels were centered across the cross-section for all panels. Panel thickness had no significant impact on surface temperature (Fig. 2.12b) since the panels are all thin enough to prevent large transverse thermal gradients. Thus, without sacrificing cooling performance, thin panels can be used to reduce weight while thicker panels can be used to provide increased mechanical protection.

2.4. Summary

Microvascular carbon/epoxy composites were explored as novel materials for battery packaging. Cooling experiments on PAN-based carbon/epoxy vascular panels showed exceptional performance at high total coolant flow rate (>20 mL min⁻¹) and channel density. Cooling performance was further improved for pitch-based carbon/epoxy panels due to their higher in-plane thermal conductivity (k ≈ 50 W m⁻¹ K⁻¹ vs. 2 W m⁻¹ K⁻¹ for PAN-based carbon/epoxy). CFD simulations using FLUENT were validated across a wide range of testing conditions by experimental data.

Further simulations confirmed that both pitch-based and PAN-based carbon/epoxy panels with sufficient channel density can satisfactorily cool EV battery packs of the Chevrolet Volt class. The
required number of 500 µm cooling channels in a 200 mm long panel are 4 for pitch-based carbon/epoxy and 16 for PAN-based carbon/epoxy. Microvascular composite panels show great promise as a lightweight, multifunctional, structural material for battery packaging.
CHAPTER 3

COMPOSITES WITH 2D CHANNEL NETWORKS FOR BATTERY PACKAGING

3.1. Introduction

The prior chapter demonstrated that carbon/epoxy composite panels with straight (1D) channels can efficiently cool EV batteries. In this chapter, recent advances in the VaSC processing technique [69] are used to fabricate composites with more advanced 2D microchannel networks. Two-dimensional vascular networks can be designed for spatially varying thermal fields and localized hot spots, while network branching offers the benefit of redundancy to circumvent channel blockages. Two-dimensional networks also allow for simple integration of cooling panels into a coolant distribution manifold (see schematic of battery packaging with 2D networks in Fig. 3.1).

Fig. 3.1. Schematic of battery packaging using microvascular composite panels with 2D channel networks. a) Battery cell is sandwiched between microvascular composite panels through which liquid coolant is pumped. b) Multiple battery cells are assembled into a battery pack with an integrated coolant distribution manifold.

* This chapter is adapted from an article submitted to International Journal of Heat and Mass Transfer [146]. The optimized network design (Fig. 3.18a) and pressure values for IGFEM simulations (in Table 3.4) were obtained in collaboration with graduate students Marcus Tan and Ahmad Najafi.
Cooling performance is compared for carbon/epoxy panels with parallel, bifurcating, serpentine, and spiral channel networks inspired by the literature [87,41,88,89,91]. The total channel volume fraction is limited to <1.6% in order to preserve mechanical properties of the composite. Cooling experiments with an IR camera and CFD simulations using ANSYS Fluent are used to compare different designs. The effect of channel size is studied for the bifurcating network, and improvements in the parallel design are demonstrated using a gradient-based computational optimization technique [102]. Finally, simulations are performed that are representative of typical operating conditions for an EV battery pack.

3.2. Materials and methods

3.2.1. Fabrication and testing of microvascular composite panels

3.2.1.1. Fabrication of sacrificial PLA networks

PLA polymer blended with 3 wt% of tin(II) oxalate catalyst (CU Aerospace) was pressed into sheets using a hot press (Tetrahedron model MP-13). Press conditions were 160 °C and 960 kPa for 10 minutes, with sheet thickness controlled by aluminum shims. Average sheet thickness was 840 µm with a standard deviation of 40 µm, based on 32 measurements across 4 networks.

Sheets were cut into 2D network templates (Fig. 3.2a) using a 90 W CO₂ laser cutter (Full Spectrum Laser, Pro Series, 48” x 36”). Network templates were designed in SolidWorks 2014 and the laser cutter was controlled using RetinaEngrave (2011) software. Three passes of the cutter were performed (at 100% speed, 4% power) to provide a complete cut with minimal edge charring. The average network width was 1020 µm with a standard deviation of 80 µm, based on 90 measurements across 4 networks. After a network template was cut from a sheet, the scrap material was recycled for reuse.

Four network designs were created (Fig. 3.3) which are denoted as the parallel, bifurcating, serpentine, and spiral designs. These networks have total channel volume fractions of 1.3 – 1.5% and up to 1.6 m channel length for the bifurcating design. To study the effect of channel size, one bifurcating network was also created at a reduced channel size of 570 ± 70 µm wide and 500 ± 10 µm tall. This network yields a channel volume fraction of 0.5%.
Fig. 3.2. Carbon fiber composite panels with 2D channel networks. a) Laser-cut PLA network with the bifurcating channel design. The network is 840 µm thick and channels are 1020 µm wide. b) Composite panel with bifurcating channel network after curing and VaSC treatment. The right half of the embedded network is outlined in black for visualization. c) Cross-sectional micrograph of a 1020 µm x 840 µm rectangular channel embedded in a carbon/epoxy composite.

Fig. 3.3. Four main network designs used for cooling studies. Note: Inlets denoted in blue (→) and outlets in red (←).

3.2.1.2. Fabrication of microvascular composite panels

Microvascular carbon/epoxy composite panels were prepared using vacuum-assisted resin transfer molding (VARTM). Twelve layers of 2 x 2 twill weave of Toray T300 3K carbon fiber (205 g m⁻², Rock West Composites) were stacked in a double vacuum bag layup, with the PLA network template placed between the 6th and 7th layer (Fig. 3.4). Epoxy resin consisting 100:35 Araldite LY 8605 epoxide to Aradur 8605 curing agent (Huntsman Advanced Materials) was mixed by hand, degassed for at least 3 hr at room temperature, and pulled through the layup using a vacuum pump at ~40 torr. Resin was cured at room temperature for 24 h and then 121 ºC for 2 h followed either 155 ºC or 177 ºC for 3 h. The heating rate was 2 ºC min⁻¹ and the cooling rate to ambient was 1 ºC min⁻¹. The final cure temperature was
reduced to 155 °C after it was observed that the PLA template was prematurely vaporizing during the 177 °C step. The final degree of cure was unaffected by this change in the cure cycle.

Average panel thickness was 2.96 ± 0.23 mm which corresponds to a fiber volume fraction of 45.4 ± 3.3% based on ASTM D3171. After cure, composite panels were cut to size with a diamond saw (MK TX3) and subject to VaSC at 200 °C and vacuum (ca. 1 torr) for 24 h to vaporize the PLA. This process yields channel networks that are the inverse replica of the PLA network template (Fig. 3.2b). The microchannels have a rectangular cross-section with slightly rounded corners, as shown in cross-sections for a 1020 µm x 840 µm channel in Fig. 3.2c and for a 570 µm x 500 µm channel in Fig. 3.5.

Fig. 3.4. Schematic of vacuum-assisted resin transfer molding (VARTM) layup.

Fig. 3.5. Cross-sectional micrograph of a 570 µm x 500 µm channel embedded in a carbon/epoxy composite panel.
3.2.1.3. Thermal testing of composites

Holes 1.03 mm in diameter were drilled 3 mm deep into the inlet and outlet of each channel to allow for 19 gauge needle fittings to be inserted. Needle fittings were secured with a two-part epoxy (JB Weld). One surface of the panel was painted with a matte black paint (Krylon) for thermal imaging. The composite specimen was then placed in the experimental setup shown in Fig. 3.6 and coupled to a polyimide flexible heater (Omega, part # KH-608/2) using thermal grease (Omega, part # Omegatherm 201). The heat flux generated by the heater was controlled by adjusting voltage with a Variac variable transformer (Staco Energy Products Co., Type L1010) while monitoring the voltage with a multimeter (Fluke Inc., Model 179). Voltage values were correlated to heat flux values using the equation $q'' = V^2/RA$ where $q''$ is areal heat flux, $V$ is voltage, $R$ is the resistance of the heater (105 Ω), and $A$ is the exposed surface area of the panel (0.03 m$^2$). The sides of the specimen were insulated with strips of fiberglass insulation and the top was exposed for IR imaging.

![Fig. 3.6. Schematics of experimental setup where panel surface temperature is recorded with an IR camera, coolant inlet and outlet temperature are measured with thermocouples embedded in tubing lines, and coolant pressure drop is measured with a pressure sensor.](image)

The coolant, 50:50 water:ethylene glycol (Macron Chemicals), was stored in a circulator (Julabo, Model F32-HP) at 21 °C and pumped through the composite with a peristaltic pump (Cole-Parmer Masterflex, Model EW-07551099). Thermocouple probes (Omega part # TMQSS-020U-36, ± 0.5 °C)
were inserted into the tubing before the inlet and after the outlet to measure coolant temperature. Thermocouple readings were processed with four-port thermocouple readers (Phidgets Inc., model #1048_0) and LabVIEW 2013. Surface temperature measurements of the panel were taken with an infrared (IR) camera (FLIR, Model SC620) with an absolute temperature accuracy of ± 2 °C.

Wet/wet gage pressure transducers (Omega, part # Px26) were used to measure pressure drop through the networks. Transducers with ranges of 0 – 35, 0 – 103, and 0 – 207 kPa were used with accuracies of 1% full scale for each. Pressure values were recorded using a DAQ board (National Instruments, NI USB-6251) and LabVIEW. To account for the pressure drop through the needle fittings, calibration tests were performed in which coolant was pumped through two fused needle fittings. Final pressure values for a cooling test were found by taking the raw pressure and subtracting the pressure required to pump coolant through the fittings (see Fig. 3.7 for the pressure adjustment vs. flow rate).

![Graph of pressure vs. flow rate](image)

**Fig. 3.7.** Pressure required to pump 50:50 water:ethylene glycol through two 19 gauge needle fittings. Fittings were 12.7 mm long and had an inner diameter of 0.75 mm. Error bars represent the maximum and minimum values for three sets of needles.

Panel surface temperature, average coolant temperature rise $\Delta T_c$, and coolant pumping pressure at steady-state were measured during experiments. The cooling efficiency ($\eta$) of the panel was then calculated as the ratio of the heat flux absorbed by the channels ($q_c$) to the total heat flux applied ($q_t$),
\[ \eta = \frac{q_c}{q_i} = \frac{\dot{m}c_p \Delta T_c}{q_i} \]  

(3.1)

where \( \dot{m} \) is coolant flow rate and \( c_p \) is the specific heat of the coolant.

### 3.2.2. CFD Simulations in ANSYS Fluent

#### 3.2.2.1. Simulation setup and boundary conditions

The commercial computational fluid dynamics (CFD) package ANSYS Fluent v15.0 was used to simulate the cooling performance of microvascular panels. Panel dimensions and boundary conditions imposed are specified in Fig. 3.8. A constant heat flux of 500 W m\(^{-2}\) was applied to the back face of the panel while the top face was open to convection and radiation. An emissivity of 0.97 was chosen to represent the matte black paint finish on the top face while a convection coefficient of \( h = 8 \) W m\(^{-2}\) K was chosen based on fitting to experimental data. The sidewalls of the panel were insulated and coolant circulated through the channel network at a set flow rate and inlet temperature of 22 °C (as measured at inlet thermocouples). A full list of panel dimensions, boundary conditions and material properties is given in Table 3.1. Coolant material properties were taken from literature [90] and composite material properties were calculated using constituent material properties (see Table 3.2).

![Fig. 3.8. Schematic of Fluent simulations. a) Top and b) side view of simulation model (dimensions in mm). The microvascular composite panel is heated from below while coolant circulates through the channel network at a set inlet temperature and flow rate. The in-plane boundaries are thermally insulated.](image-url)
Table 3.1. Panel dimensions, boundary conditions, and material properties used for Fluent simulations.

<table>
<thead>
<tr>
<th>Simulation Condition</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Plate dimensions</strong></td>
<td></td>
</tr>
<tr>
<td>Plate width (mm)</td>
<td>150</td>
</tr>
<tr>
<td>Plate height (mm)</td>
<td>200</td>
</tr>
<tr>
<td>Plate thickness (mm)</td>
<td>3</td>
</tr>
<tr>
<td>Channel width (mm)</td>
<td>1.02a</td>
</tr>
<tr>
<td>Channel thickness (mm)</td>
<td>0.84a</td>
</tr>
<tr>
<td><strong>Boundary conditions</strong></td>
<td></td>
</tr>
<tr>
<td>Total coolant flow rate (mL min(^{-1}))</td>
<td>28.2(^b)</td>
</tr>
<tr>
<td>Coolant inlet temperature (°C)</td>
<td>22</td>
</tr>
<tr>
<td>Coolant outlet pressure (Pa)</td>
<td>0</td>
</tr>
<tr>
<td>Supplied heat flux (W m(^{-2}))</td>
<td>500 from back(^c)</td>
</tr>
<tr>
<td>Convection coefficient (h) for top face (W m(^{-2})K(^{-1}))</td>
<td>8</td>
</tr>
<tr>
<td>Emissivity for top face</td>
<td>0.97</td>
</tr>
<tr>
<td>Air temperature surrounding top face (°C)</td>
<td>21</td>
</tr>
<tr>
<td>Sides of panel</td>
<td>Insulated</td>
</tr>
<tr>
<td><strong>Coolant - 50/50 water-ethylene glycol(^d)</strong></td>
<td></td>
</tr>
<tr>
<td>Density (kg m(^{-3}))</td>
<td>1065</td>
</tr>
<tr>
<td>Viscosity (kg m(^{-1})s(^{-1}))</td>
<td>0.0069((T/273)^{0.3})</td>
</tr>
<tr>
<td>Specific heat (J kg(^{-1})K(^{-1}))</td>
<td>2574.7 + 3.0655(T)</td>
</tr>
<tr>
<td>Thermal conductivity (W m(^{-1})K(^{-1}))</td>
<td>0.419</td>
</tr>
<tr>
<td><strong>Panel – Carbon fiber reinforced epoxy</strong></td>
<td></td>
</tr>
<tr>
<td>Fiber volume fraction (V_f) (%)</td>
<td>45</td>
</tr>
<tr>
<td>Density (kg m(^{-3}))</td>
<td>1410(^e)</td>
</tr>
<tr>
<td>Specific heat (J kg(^{-1})K(^{-1}))</td>
<td>890(^e)</td>
</tr>
<tr>
<td>In-plane thermal conductivity (W m(^{-1})K(^{-1}))</td>
<td>2.04(^e)</td>
</tr>
<tr>
<td>Transverse thermal conductivity (W m(^{-1})K(^{-1}))</td>
<td>0.43(^e)</td>
</tr>
</tbody>
</table>

\(^a\) Channel dimensions of 0.94 mm wide x 0.80 mm thick were also simulated to provide bounds for pressure. For one panel with smaller channels, dimensions of 0.57 mm x 0.50 mm and 0.50 mm x 0.49 mm were simulated.

\(^b\) This is the baseline value: other values also tested

\(^c\) This is the average value for heat flux: see Fig. 3.9 for distribution.

\(^d\) Properties taken from [90]

\(^e\) Determined using constituent properties and \(V_f\). Constituent properties are given in Table 3.2. Density and specific heat were determined with rule of mixtures. Conductivity was determined with rule of mixtures for axial conductivity and the self-consistent model for transverse conductivity; see [79] for calculations.
Table 3.2. Material properties for epoxy resin and carbon fiber fabric.

<table>
<thead>
<tr>
<th>Constituent Material Property</th>
<th>Value</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>Araldite 8605 epoxy</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Density (kg m(^{-3}))</td>
<td>1120</td>
<td>Measured with ASTM D792</td>
</tr>
<tr>
<td>Specific heat (J kg(^{-1})K(^{-1}))</td>
<td>1000</td>
<td>Manufacturer value</td>
</tr>
<tr>
<td>Thermal conductivity (W m(^{-1})K(^{-1}))</td>
<td>0.2</td>
<td>Manufacturer value</td>
</tr>
<tr>
<td>Toray T300 carbon fiber</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Density (kg m(^{-3}))</td>
<td>1760</td>
<td>Manufacturer value</td>
</tr>
<tr>
<td>Specific heat (J kg(^{-1})K(^{-1}))</td>
<td>800</td>
<td>Manufacturer value</td>
</tr>
<tr>
<td>In-plane thermal conductivity (W m(^{-1})K(^{-1}))</td>
<td>7.9</td>
<td>[1]</td>
</tr>
<tr>
<td>Transverse thermal conductivity (W m(^{-1})K(^{-1}))</td>
<td>2.0</td>
<td>[2]</td>
</tr>
</tbody>
</table>

To provide bounds for pressure predictions, two sets of channel dimensions were simulated: one with average channel dimensions and one with dimensions reduced by one standard deviation. For all panels but one, the average channel dimensions were 1020 µm x 840 µm and the reduced dimensions were 940 µm x 800 µm. For the bifurcating panel with smaller channels, the average dimensions were 570 µm x 500 µm and the reduced dimensions were 500 µm x 490 µm. Since the pumping pressure scales with the 4\(^{th}\) power of the diameter, any slight restriction in channel dimension can dramatically influence the pressure prediction. We use these two simulation conditions to bound the experimental data.

3.2.2.2. Modeling of nonuniform heat flux

The polyimide resistive heater used in experiments provided a slightly nonuniform heat flux (Fig. 3.9a). To account for this nonuniformity, a 4\(^{th}\)-order polynomial for heat flux was fit to the IR temperature data using MATLAB and the polynomial was used in Fluent simulations (Fig. 3.9b).

The nonuniform heat flux from the heater was modeled as follows. The heater was lightly covered with thermal grease, placed on a balsa wood platform, and imaged with the IR camera at 500 W m\(^{-2}\) total applied heat flux. A steady-state image (Fig. 3.9a) was used to extract values of temperature (\(T\)) across the xy plane of the heater. The 150 mm x 200 mm heater was imaged with 390 x 520 pixels.

Temperature values at each pixel were converted to a temperature increase from room temperature, \(\Delta T = T - T_{ambient}\) where \(T_{ambient} = 21^\circ\)C. \(\Delta T\) values were normalized by the average \(\Delta T\) value, i.e. \(\Delta T_{norm} = \Delta T / \langle \Delta T \rangle\). A 4\(^{th}\)-order polynomial was fit to \(\Delta T_{norm}\) using a linear least-squares regression in the
“Curve Fitting” application in MATLAB. The 4\textsuperscript{th}-order polynomial provided an accurate fit to IR data ($R^2 = 0.96$) and did not lead to noticeably longer simulation times.

Fig 3.9. Heater characterization used in cooling tests. a) Steady-state IR surface temperature profile of the heater for a nominal heat flux of 500 W m\textsuperscript{-2}. b) Simulated temperature profile of the heater with a non-uniform heat flux obtained from a 4\textsuperscript{th}-order polynomial fit of the experimental temperature field.

The supplied heat flux at any pixel is assumed proportional to $\Delta T_{\text{norm}}$. This assumption follows from the following conditions: (1) all supplied heat is lost from radiation/convection to the heater surface, (2) conduction of heat through the heater itself is negligible, and (3) the heat lost to the surface can be modeled as $q'' = h_{\text{total}} \Delta T$ where $q''$ is areal heat flux and $h_{\text{total}}$ is a convection coefficient that accounts for both convection and radiation. As shown in Tan et al. [102], a constant value of $h_{\text{total}} = 13.6$ W m\textsuperscript{-2} K can accurately capture the effect of both convection and radiation for this system.

The 4\textsuperscript{th}-order fit to $\Delta T_{\text{norm}}$ is then multiplied by $h_{\text{total}}$ to model the nonuniform heat flux, giving

$$q'' = 500 * (0.2489 + 23.42 x + 21.87 y - 485.7 x^2 - 16.96 xy - 356.8 y^2 + 4317 x^3 + 170.4 x^2 y + 276.7 xy^2 + 2422 y^3 - 4060 x^4 - 791 x^3 y - 490 x^2 y^2 - 972.2 xy^3 - 5849 y^4) \text{ W m}^2$$ \hspace{1cm} (3.2)

where $x$ and $y$ are the surface coordinates in unit of meter. The heat flux was implemented in Fluent using a user-defined function written in C.

3.2.2.3. Mesh generation and convergence

Tetrahedral finite volume meshes of panels were constructed in ANSYS Meshing v15.0. To test for convergence, meshes were created at successively smaller grid sizes for the parallel network design with
a channel size of 1020 µm x 840 µm. The fluid element sizing had by far the greatest influence on convergence, with a 170 µm sizing required for convergence of temperature (within 0.1 °C) and pressure (within 2%, see Fig. 3.10). Critical fluid element sizes of 130 µm, 100 µm, and 80 µm were then found for channel sizes of 940 µm x 800 µm, 570 µm x 500 µm, and 500 µm x 490 µm, respectively. The final meshes contained from 1.1 – 3.2 million fluid elements and 6.3 - 17.3 million total elements.

![Mesh convergence plot showing simulated pumping pressure for the parallel network at a channel size of 1020 µm x 840 µm and 28.2 mL min⁻¹ flow rate.](image)

Steady-state simulations were performed using the SIMPLE pressure-velocity coupling scheme, Green-Gauss node-based gradient discretization, second-order pressure discretization, third-order MUSCL momentum discretization, and third-order MUSCL energy discretization. These simulations solve for the conservation of mass, momentum, and energy for an incompressible, Newtonian fluid with laminar flow. The maximum Reynolds number for any simulation was ca. 500, confirming the assumption of laminar flow. The convergence criterion used was for velocity and continuity residuals to reach $10^{-3}$ and for the energy residual to reach $10^{-8}$. These thresholds were sufficient for convergence of temperature and pressure fields.
3.3. Results and discussion

3.3.1. Thermal and pressure targets for cooling panels

The heater size and supplied heat flux were chosen to represent batteries in the Chevy Volt [90,91,106]. Tests were performed with the assumption of a 1:1 battery:cooling panel ratio (e.g. the stacking sequence in Fig. 3.1b), so the heater was powered sufficiently to provide a heat flux representative of one battery in the Volt (500 W m$^{-2}$ nominal). The choice of coolant (50:50 water:ethylene glycol) and baseline flow rate (28.2 mL min$^{-1}$, or 0.5 g s$^{-1}$) were also chosen to match the Volt cooling system [90,91,106].

The goal for a battery cooling panel is to minimize panel temperature since lower battery temperature leads to reduced side reactions and longer lifetime [8,9]. The standard deviation of temperature across the panel, $\sigma_T$, should also be minimized since variations in battery temperature lead to uneven charge/discharge profiles across the battery. Finally, pressure should be minimized to reduce pumping power. Current vehicle cooling systems typically operate at pressures of < 140 kPa [107].

3.3.2. Effect of flow rate

The effect of flow rate on cooling performance was first investigated for the parallel network panel (Fig. 3.11). With no coolant flow, maximum panel temperature reaches approximately 60 °C. As flow rate increases, the maximum surface temperature ($T_{\text{max}}$) drops quickly as heat is removed by the coolant. At higher flow rates, $T_{\text{max}}$ (as well as average surface temperature $T_{\text{av}}$ and $\sigma_T$, see Table 3.3) reaches a plateau value. Higher flow rates result in smaller values of $\Delta T_c$ as the outlet coolant temperature approaches the inlet temperature (Table 3.3). Cooling efficiency $\eta$ improves with flow rate and then plateaus reaching roughly 75% at the highest flow rate tested (56.4 mL min$^{-1}$). Good agreement was obtained between experimental data and simulations for surface temperature profiles and cooling efficiency. Small discrepancies between experimental and simulated values are likely due to experimental error or variation of the convection coefficient (see discussion in section 3.3.6).
Fig 3.11. Effect of flow rate on cooling performance for the parallel network panel. a) Maximum panel temperature $T_{\text{max}}$ and cooling efficiency $\eta$ as a function of flow rate. Error bars represent the maximum and minimum values for two replicate panels. b) Experimental and simulated surface temperature profiles at various flow rates. Inlet and outlet locations are denoted by blue (←) and red arrows (→). Simulations were performed using nominal channel dimensions.

Table 3.3. Values of channel volume fraction $V_C$, average surface temperature $T_{\text{av}}$, maximum surface temperature $T_{\text{max}}$, standard deviation of surface temperature $\sigma_T$, and coolant temperature rise $\Delta T_c$ for all experiments. Simulated values using nominal dimensions are shown for comparison. Experimental variation gives the maximum and minimum values for $N$ replicate panels.

<table>
<thead>
<tr>
<th>Network</th>
<th>$V_C$ (%)</th>
<th>$N$</th>
<th>Flow rate (ml min$^{-1}$)</th>
<th>$T_{\text{av}}$ (°C)</th>
<th>$T_{\text{max}}$ (°C)</th>
<th>$\sigma_T$ (°C)</th>
<th>$\Delta T_c$ (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>Sim.</td>
<td>Exp.</td>
<td>Sim.</td>
<td>Exp.</td>
</tr>
<tr>
<td>Parallel</td>
<td>1.5</td>
<td>2</td>
<td>0</td>
<td>55.1</td>
<td>53.6 ± 0.1</td>
<td>59.3</td>
<td>59.6 ± 0.2</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>&quot;</td>
<td>7.1</td>
<td>36.7</td>
<td>37.9 ± 1.0</td>
<td>49.1</td>
<td>49.2 ± 1.4</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>&quot;</td>
<td>14.1</td>
<td>32.1</td>
<td>33.7 ± 0.6</td>
<td>42.2</td>
<td>43.2 ± 1.0</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>&quot;</td>
<td>28.2</td>
<td>29.0</td>
<td>30.4 ± 0.2</td>
<td>36.5</td>
<td>37.2 ± 0.6</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>&quot;</td>
<td>42.3</td>
<td>28.0</td>
<td>29.2 ± 0.2</td>
<td>34.2</td>
<td>34.8 ± 0.5</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>&quot;</td>
<td>56.4</td>
<td>27.4</td>
<td>28.4 ± 0.1</td>
<td>32.9</td>
<td>33.1 ± 0.2</td>
</tr>
<tr>
<td>Bifurcating</td>
<td>1.5</td>
<td>2</td>
<td>28.2</td>
<td>28.8</td>
<td>30.5 ± 0.2</td>
<td>33.0</td>
<td>35.1 ± 0.5</td>
</tr>
<tr>
<td>Serpentine</td>
<td>1.3</td>
<td>&quot;</td>
<td>&quot;</td>
<td>28.6</td>
<td>29.9 ± 0.1</td>
<td>32.6</td>
<td>33.8 ± 0.2</td>
</tr>
<tr>
<td>Spiral</td>
<td>1.4</td>
<td>&quot;</td>
<td>&quot;</td>
<td>28.4</td>
<td>30.0 ± 0.2</td>
<td>31.5</td>
<td>33.2 ± 0.1</td>
</tr>
<tr>
<td>Bifurcating$^a$</td>
<td>0.5</td>
<td>1</td>
<td>28.2</td>
<td>29.2</td>
<td>30.8 ± 0.2</td>
<td>33.3</td>
<td>35.6 ± 0.2</td>
</tr>
<tr>
<td>Optimized</td>
<td>1.6</td>
<td>&quot;</td>
<td>&quot;</td>
<td>28.4</td>
<td>29.7 ± 0.2</td>
<td>33.1</td>
<td>33.9 ± 0.2</td>
</tr>
</tbody>
</table>

$^a$Bifurcating network with channel size of 570 μm x 500 μm, vs. 1020 μm x 840 μm channel size for all other samples.
Pumping pressure rises almost linearly with flow rate (Fig. 3.12a). Any nonlinearity is attributed to pressure losses at corners and at locations where fluid streams combine and separate [108–110]. The maximum pressure measured (25 kPa) is well within the range of commercial coolant pumps. Two sets of simulations were performed in order to bound the pumping pressure. The first used nominal (average) dimensions for the channel cross-section (1020 µm x 840 µm). A second simulation was performed using a cross-section of 940 µm x 800 µm which represents one standard deviation below nominal. Given the extreme sensitivity of pressure with respect to hydraulic diameter, any decrease in nominal cross-sectional area can lead to a large increase in pressure. Both simulations bound the experimental data well.

Pumping power was calculated by multiplying coolant pressure by flow rate and is plotted in Fig. 3.12b. The power increases with the square of the flow rate as expected. The magnitude of pumping power is small at all flow rates considered and well within available power limits for an EV battery.

Fig. 3.12. Pumping pressure and power for the parallel network panel. a) Experimental and simulated pressure vs. flow rate and b) power vs. flow rate. Simulations are performed using the average channel dimensions (1020 µm x 840 µm) as well as a reduced (by one standard deviation) channel cross-section (940 µm x 800 µm). Error bars represent the maximum and minimum values for two replicate panels.
3.3.3. Effect of network design

3.3.3.1. Thermal performance

Four different vascular networks with similar volume and inter-channel spacing (Fig. 3.3) were fabricated for comparison studies. Two branched networks (parallel and bifurcating) and two single-channel networks (serpentine and spiral) were chosen, inspired by prior cooling studies for batteries and fuel cells [87,41,88–91]. Cooling panels with each of these networks were tested at 28.2 mL min\(^{-1}\) flow rate for comparison (Fig. 3.13). Both simulated and experimental temperature profiles agree well in all cases. For all networks, hot spots form between cooling channels as a result of relatively slow heat conduction to the channel surface. Temperature generally increases in the direction of coolant flow due to heat transfer into the coolant. Both \(T_{\text{av}}\) and \(\Delta T_c\) are similar for all panel designs (Table 3.3).

![Fig 3.13.](Image)

Both \(T_{\text{max}}\) and \(\sigma_T\) are more affected by network design (Fig. 3.14). The parallel network has the highest values of \(T_{\text{max}}\) and \(\sigma_T\) since it develops a large hot spot near the center of the panel. Simulations suggest the hot spot forms due to uneven flow distribution, with the least flow moving through the center of the panel (Fig. 3.15). In contrast, the bifurcating network shows a very uniform flow distribution and correspondingly better thermal performance.
Fig 3.14. Comparison of thermal performance for all network designs. a) Maximum surface temperature $T_{\text{max}}$ and b) standard deviation of surface temperature $\sigma_T$, with error bars representing maximum and minimum values for 2 replicate panels. The flow rate was 28.2 mL min$^{-1}$ for all cases. Simulations were performed with nominal channel dimensions.

Fig. 3.15. Simulated flow rate distribution through branched networks at 28.2 mL min$^{-1}$ total flow rate. The channels are numbered from top to bottom of the panel. Simulations were performed with nominal channel dimensions.

The serpentine network shows slightly better thermal performance than the bifurcating network, likely due to the presence of counter flow in adjacent branches of the network. The spiral network has the best thermal performance of all designs considered, with the lowest $T_{\text{max}}$ and $\sigma_T$. This is likely because the coolant moves around the periphery, mitigating the thermal hot spot at the outlet location of the serpentine design.
3.3.3.2. Pressure performance

The parallel and bifurcating networks have similar pumping pressures at 28.2 mL min\(^{-1}\) flow rate and are both quite low in magnitude (8 – 10 kPa) (Fig. 3.16). The serpentine and spiral networks require pumping pressures that are an order of magnitude higher (100 – 110 kPa) and approaching the upper limit of typical vehicle coolant pumps. This large disparity in required pressure is a result of the division of flow in the branched networks, which results in both shorter channel lengths and lower flow rate within each channel. Nevertheless, pumping power requirements for the serpentine and spiral networks are low in magnitude (< 60 mW).

![Figure 3.16](image)

**Fig. 3.16.** Pumping pressure and power for the four network designs evaluated. Simulations were performed using average channel dimensions (1020 µm x 840 µm) and channel dimensions reduced by one standard deviation (940 µm x 800 µm). Error bars represent the maximum and minimum values for two replicate panels. Flow rate was 28.2 mL min\(^{-1}\) in all cases.

The pressure drop through these networks arises from both viscous forces along the length of the channels and additional pressure losses from corners and branch points. The pressure drop associated with corners and branch points in microchannels is negligible for very low Reynolds number (\(Re < 10\)), significant and well-predicted for turbulent flow (\(Re > 2000\)), and difficult to predict for intermediate \(Re\) values such as those in these panels (50 < \(Re < 500\) for these panels) [108–110]. Pressure losses at corners and branch points increase with increasing \(Re\) number, larger numbers of corners/branch points...
per total channel length, and lower radii of curvature at corners/branch points [108–110]. It is worth noting that the networks here all contained 0.3 mm radius fillets to reduce such pressure losses.

The relative contribution of corners and branch points to overall pressure drop was estimated by comparing Fluent simulations to simulations using dimensionally-reduced hydraulic models. The dimensionally-reduced models were implemented using the interface-enriched generalized finite element method (IGFEM) in a collaborative study [102]. These simulations calculate pressure drop based solely on laminar viscous forces along the lengths of the channel. Pumping pressure values from Fluent and IGFEM are compared in Table 3.4 for all networks at 28.2 mL min\(^{-1}\) flow rate. Fluent simulations provide 4% - 16% higher pressure than IGFEM, which is attributed to additional losses at corners and branch points. The single-channel networks have the largest disagreement in pressure, which is expected since these networks have no flow division so fluid has a high \(Re\) number throughout the network.

Table 3.4. Comparison of pumping pressure from Fluent and IGFEM simulations for different networks at 28.2 mL min\(^{-1}\) flow rate. The channel size was 940 µm x 800 µm.

<table>
<thead>
<tr>
<th>Network</th>
<th>(P_{\text{IGFEM}}) (kPa)</th>
<th>(P_{\text{Fluent}}) (kPa)</th>
<th>% difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>Parallel</td>
<td>10.5</td>
<td>10.9</td>
<td>4</td>
</tr>
<tr>
<td>Bifurcating</td>
<td>7.9</td>
<td>8.6</td>
<td>9</td>
</tr>
<tr>
<td>Serpentine</td>
<td>112</td>
<td>130</td>
<td>16</td>
</tr>
<tr>
<td>Spiral</td>
<td>105</td>
<td>121</td>
<td>15</td>
</tr>
</tbody>
</table>

Considering both temperature and pressure objectives, the spiral and bifurcating network designs offer the best overall performance. The spiral network offers the best thermal performance at feasible pumping pressures. However, the bifurcating network is likely a better overall choice since it has nearly comparable thermal performance, but at much lower pumping pressures. The lower required pumping pressure means that higher flow rates could be easily accessed to improve thermal performance. The presence of multiple channels offers redundancy to mitigate the effect of channel blockages. In addition, the bifurcating network can easily be accommodate more channels (e.g. 16 channels instead of 8), which would increase channel density and decrease the panel temperature as was previously shown for 1D channels [106]. Conversely, it would be difficult to increase channel density for both the serpentine and spiral networks since this would increase the overall channel length and required pumping pressure.
3.3.4. Effect of channel size

The nominal channel size investigated (1020 µm x 840 µm) is similar to those used in commercial (aluminum) cooling panels. Smaller channels can be advantageous for composite panels, however, since they are less likely to disrupt fiber architecture and thus, less likely to reduce mechanical properties [92]. To demonstrate the use of smaller channels, a bifurcating network composite panel was fabricated with a channel size of 570 ± 70 µm wide and 500 ± 10 µm tall. The panel was tested at a flow rate of 28.2 mL min⁻¹ and gives a nearly identical thermal profile to the bifurcating network panel with larger channels (see Fig. 3.17 and Table 3.3). The penalty for using smaller channels is that pumping pressure increases significantly from ~8 kPa for the larger channel network to ~60 kPa for the smaller channel network. However, the required pressure is still well within the range of commercial pumps.

![Fig. 3.17. Surface temperature profiles and pumping pressure for a cooling panel with the bifurcating network and 570 µm x 500 µm channels at 28.2 mL min⁻¹ flow rate. Simulations were performed with nominal channel dimensions and dimensions reduced by one standard deviation (500 µm x 490 µm) to give bounds for pressure. The simulated temperature profile shown is for nominal dimensions.](image)

3.3.5. Improvements in channel design using gradient-based optimization

The four network designs presented in Fig. 3.3 were selected *a priori*, but improved performance can be obtained through computational optimization. In Tan et al. [102], a gradient-based optimization scheme was used to improve the thermal performance of panels based on a parallel network design. Network nodes were iteratively moved within the panel in order to reduce the maximum temperature of the panel. Computational efficiency was obtained by combining simplified thermal and hydraulics models with the interface-enriched generalized finite element method (IGFEM).
A panel with the optimized parallel network (Fig. 3.18a) was fabricated and compared in performance to the nominal parallel network design. The optimal network design features diagonal channels that branch off closer to the channel inlet than the nominal parallel design. This yields a more uniform flow distribution through the branches (Fig. 3.15) and a correspondingly lower panel temperature (Fig. 3.18b-c). The required pumping pressure through the optimal network was nearly unchanged from the nominal design (Fig. 3.19).

**Fig. 3.18.** Thermal performance for an optimized version of the parallel network design. a) Optimal network design (minimum temperature) from gradient-based optimization scheme [102] of the placement of network nodes. The original (parallel) network is shown in black while the optimized network is shown in red. b) Simulated (left) and experimental (right) surface temperature profiles of the optimized network at a flow rate of 28.2 mL min⁻¹. Inlet and outlet locations are denoted by blue (→) and red arrows (→). c) Comparison of maximum temperature for the nominal and optimized designs. Error bars represent the maximum and minimum values for two replicate panels. Simulations were performed with nominal channel dimensions.

**Fig. 3.19.** Pumping pressure at 28.2 mL min⁻¹ for the nominal (parallel) and optimized networks. Simulations were performed using average channel dimensions (1020 µm x 840 µm) and channel dimensions reduced by one standard deviation (940 µm x 800 µm). Error bars represent the maximum and minimum values for two replicate panels.
3.3.6. Discrepancies between experiment and simulation

Experimental temperatures were typically 1 – 2 °C higher than simulation. The maximum error for either $T_{av}$ or $T_{\text{max}}$ was 2.3 °C, just outside the 2 °C accuracy window of the IR camera. Other errors could arise from the thermal conductivity value of the composite, since it was calculated using constituent properties instead of being directly measured. In addition, a single value of the convection coefficient $h$ was chosen for all simulations, but $h$ likely varies with different operating conditions.

Experimental and simulated $\sigma_T$ agreed within 0.4 °C for all cases except for the case of no flow, where the error was 1.4 °C. The error for the zero flow rate case most likely comes from an overestimated thermal conductivity in simulations. For pumping pressure, experiments were fully bounded by simulations using average and reduced channel dimensions.

For $\Delta T_c$, the thermocouples have a listed accuracy of 0.5 °C so subtracting inlet from outlet temperatures leads to a 1 °C accuracy window. The maximum error in $\Delta T_c$ was 0.7 °C, within this range. Note that the apparent disparity in $\eta$ at high flow rate (see Fig. 3.11) is due to the low magnitude of $\Delta T_c$ at these rates, which makes the data more sensitive to measurement error.

3.3.7. Simulations more closely representing an EV battery pack

The Fluent simulations in Sections 3.2 – 3.5 were validated by experimental correlation. However, cooling panels in an actual battery pack would be heated by batteries from both sides (see Fig. 1b) instead of having one face open to convection/radiation. In addition, the coolant inlet temperature would likely be higher than room temperature [90,91] and commercial composite panels would be manufactured at higher fiber volume fraction, giving higher thermal conductivity. Simulations were performed with these changes (Table 3.5) for the optimized network design. Again, the cooling objective was to maintain $T_{\text{max}}$ below 40 °C.
Table 3.5. Modifications to the values in Table 3.1 for simulation of a cooling panel in an EV battery pack.

<table>
<thead>
<tr>
<th>Simulation Condition</th>
<th>Modified value</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Boundary conditions</strong></td>
<td></td>
</tr>
<tr>
<td>Coolant inlet temperature (°C)</td>
<td>27</td>
</tr>
<tr>
<td>Supplied heat flux (W m$^{-2}$)</td>
<td>250 from top and 250 from bottom $^a$</td>
</tr>
<tr>
<td><strong>Panel – Carbon fiber reinforced epoxy</strong></td>
<td></td>
</tr>
<tr>
<td>Fiber volume fraction (%)</td>
<td>60</td>
</tr>
<tr>
<td>Density (kg m$^{-3}$)</td>
<td>1500</td>
</tr>
<tr>
<td>Specific heat (J kg$^{-1}$ K$^{-1}$)</td>
<td>860</td>
</tr>
<tr>
<td>In-plane thermal conductivity (W m$^{-1}$ K$^{-1}$)</td>
<td>2.7</td>
</tr>
<tr>
<td>Transverse thermal conductivity (W m$^{-1}$ K$^{-1}$)</td>
<td>0.59</td>
</tr>
</tbody>
</table>

$^a$ For the baseline case of 500 W m$^{-2}$ total, the nonuniform heat flux from Fig. 3.9 was simulated but with half the magnitude applied to both sides. Other total heat flux values were simulated by linearly scaling the heat flux distribution.

For a total applied heat flux of 500 W m$^{-2}$, the panel reaches $T_{max} \approx 40$ °C at a flow rate of approximately 30 mL min$^{-1}$ (Fig. 3.20). Thus, the panel reaches the target temperature near the baseline flow rate as desired. For a supplied heat flux of 250 W m$^{-2}$, which is representative of lower power batteries or a stacking sequence where there are two panels per battery, a flow rate of only $\sim$15 mL min$^{-1}$ is required. For higher heat fluxes of 750 and 1000 W m$^{-2}$, the panel cannot reach the target temperature for the flow rates investigated. However, the target temperature would likely be reached by increasing the density of the channel network as was previously shown for panels with 1D channels [106].
Fig. 3.20. Thermal performance of a panel with the optimized network design subject to EV battery pack conditions (see Table 3.5 for simulation parameters). The legend denotes the total applied heat flux which is evenly divided between both sides (top and bottom) of the cooling panel.

3.4. Summary

Microvascular carbon fiber composites could represent a new class of strong, lightweight, thermally responsive battery packaging materials. The VaSC processing technique enables the fabrication of carbon fiber composites with complex, 2D, interconnected vascular networks. Composite panels with a variety of different vascular networks were fabricated and tested under an applied heat flux (500 W m$^{-2}$) and coolant flow rate (28.2 mL min$^{-1}$) representative of typical EV battery cooling conditions. The spiral network offers the best thermal performance with relatively high pumping pressure (>100 kPa) while the bifurcating network offers good thermal performance at much reduced pressure (<10 kPa). Large hot spots form on panels with the nominal parallel network, but these hot spots can be reduced by modifying the network based on the results of a gradient-based optimization scheme. Panel cooling performance was unaffected by channel size.

CFD simulations were validated by experiment for all panels and operating conditions tested. Further simulations confirm that composite panels can sufficiently cool an EV battery pack below 40 °C. Work is ongoing to design carbon fiber cooling panels for fuel cells, microelectronics, antennas [43], and satellites [44].
CHAPTER 4

BLOCKAGE-TOLERANT MICROVASCULAR COOLING NETWORKS

4.1. Introduction

The previous two chapters demonstrated that microchannels can efficiently cool composite panels while occupying a small volume fraction of the host material. However, due to their small size, microchannels are susceptible to blockages that may arise from contaminants in the coolant or damage [111,112]. Blockages can lead to sudden increases in panel temperature, especially for channel networks with few interconnections [113]. Blockages are also a major concern in self-healing systems to ensure robust and repeated healing capability [70,114].

In nature, blockages in microvascular networks are circumvented through network redundancy. For example, palmate leaves possess dense venation to allow for water and nutrient transport even if the main vein is damaged [53,54,82]. Mammal cardiovascular systems similarly have dense, hierarchical vasculature to allow for blood flow even when arteries or veins become clogged [115–117]. A limited number of simulation studies have been performed to design synthetic microvascular networks with similar redundancy for cooling applications [81,108,113]. However, cooling panels have never been optimized specifically for blockage tolerance. In addition, no study has experimentally demonstrated the blockage tolerance of microvascular cooling panels.

Here we optimize cooling networks in microvascular polydimethylsiloxane (PDMS) panels to resist temperature changes when blocked. Microvascular PDMS was used due to the ease of manufacturing and ability to create blockages wherever needed for validation. Panels are simulated using a high-speed technique combining the interface-enriched generalized finite element method (IGFEM) and reduced-order fluid and thermal models [118]. The simulation method is validated through thermography experiments on PDMS panels fabricated with a novel laser-cutting and secondary bonding technique.

* This chapter is adapted from an article to be submitted to Applied Thermal Engineering [147]. Simulation results are from collaboration with graduate students Marcus Tan and Ahmad Najafi. Simulation figures (Figs. 4.2, 4.7-10, and parts of other figures) are from Marcus Tan.
Gradient-based optimization is then used to minimize panel temperature while the panel is subject to its single worst-case blockage (the blockage that results in the highest temperature rise). Optimized networks are developed and tested for nodal degrees (a measure of redundancy) of 2 – 6.

4.2. Materials and methods

4.2.1. Simulation of microvascular PDMS panels

The geometry modeled is shown in Fig. 4.1 and the dimensions, boundary conditions, and material properties used are listed in Table 4.1. A 75 mm x 75 mm x 3.8 mm PDMS microvascular panel was heated from below with a 2000 W m\(^{-2}\) heat flux over the central 50 mm x 50 mm. Water/glycol coolant was pumped through a 2D microvascular network formed by 0.81 mm x 0.55 mm interconnected rectangular channels.

![Schematic of microvascular PDMS cooling panels from the a) top and b) side views with dimensions in mm. The panel is heated in the central 50 mm x 50 mm region from below coolant circulates at a set inlet temperature and flow rate. The top of the panel is open to convection/radiation and the sides are insulated.](image-url)
Table 4.1. Dimensions, boundary conditions, and material properties for cooling simulations.

<table>
<thead>
<tr>
<th>Simulation Condition</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Panel dimensions in mm</strong></td>
<td></td>
</tr>
<tr>
<td>Panel width and length</td>
<td>75</td>
</tr>
<tr>
<td>Panel thickness</td>
<td>3.8</td>
</tr>
<tr>
<td>Channel width</td>
<td>0.81 (a)</td>
</tr>
<tr>
<td>Channel thickness</td>
<td>0.55 (a)</td>
</tr>
<tr>
<td><strong>Boundary conditions</strong></td>
<td></td>
</tr>
<tr>
<td>Baseline coolant flow rate (mL min(^{-1}))</td>
<td>28.2</td>
</tr>
<tr>
<td>Coolant inlet temperature (°C)</td>
<td>22</td>
</tr>
<tr>
<td>Coolant outlet pressure (Pa)</td>
<td>0</td>
</tr>
<tr>
<td>Supplied heat flux (W m(^{-2}))</td>
<td>2000</td>
</tr>
<tr>
<td>Heater width and length (mm)</td>
<td>50</td>
</tr>
<tr>
<td>Convection coefficient (h) for top face (W m(^{-2}) K(^{-1}))</td>
<td>15</td>
</tr>
<tr>
<td>Emissivity for top face</td>
<td>0.97</td>
</tr>
<tr>
<td>Air temperature surrounding top face (°C)</td>
<td>22</td>
</tr>
<tr>
<td>Sides of panel</td>
<td>Insulated</td>
</tr>
</tbody>
</table>

**Coolant - 50/50 water-ethylene glycol [90]**

- Density (kg m\(^{-3}\)) 1065
- Viscosity (kg m\(^{-1}\) s\(^{-1}\)) 0.0069\(^{-4.3}\)(7/273)
- Specific heat (J kg\(^{-1}\) K\(^{-1}\)) 2574.7 + 3.0655\(T\)
- Thermal conductivity (W m\(^{-1}\) K\(^{-1}\)) 0.419

**Panel – Polydimethylsiloxane (PDMS) [119]**

- Thermal conductivity (W m\(^{-1}\) K\(^{-1}\)) 0.27

\(a\) Channel dimensions of 0.68 mm wide x 0.53 mm thick were also simulated to provide bounds for pressure. These values represent the nominal (average) dimensions minus one standard deviation.

The top panel surface was open to the environment where convective and radiative heat loss occurs. The convective heat loss coefficient \(h = 15\) W m\(^{-2}\) K\(^{-1}\) was found by fitting to experimental data and panel emissivity \(\varepsilon\) was set to 0.97 to represent the matte black paint used on experimental specimens. The sides of the sample were insulated and an inlet temperature of 22 °C was specified based on the average inlet temperature measured during experiments. Coolant flow rate ranged from 0 mL min\(^{-1}\) to 56.4 mL min\(^{-1}\) for the validation study and was set to 28.2 mL min\(^{-1}\) for the optimization study.

To increase simulation speed, simulations were performed on a 2D model of the test geometry with channels collapsed into line source/sinks. Simulations were implemented using the interface-enriched generalized finite element method (IGFEM) with reduced-order thermal and fluid models. A detailed
description of the models is given in publications by Tan, Geubelle and coworkers [118,120]. Simulations were performed using a 40 x 40 mesh size.

Two sets of channel dimensions were simulated to provide bounds for pressure: one with nominal (average) channel dimensions of 0.81 mm x 0.55 mm and one with channel dimensions reduced by one standard deviation (0.68 mm x 0.53 mm). The reduced dimensions were used since the pumping pressure scales with the fourth power of the diameter, so any slight restriction in channel dimension can dramatically influence the pressure prediction. Note that changing the channel dimension has no impact on simulated thermal fields, since the channels are modeled as line source/sinks.

4.2.2. Gradient-based optimization setup

Gradient-based optimization was performed to reduce panel temperature by moving channel nodes as described in [102,120]. The objective function was to minimize the maximum panel temperature $T_{\text{max}}$. However, since $T_{\text{max}}$ is not differentiable, it was replaced was the differential $p$-norm of temperature

$$\| T \|_p = \left( \int_{\Omega} T^p d\Omega \right)^{1/p}$$

where $\Omega$ is the panel domain and $p$ is an integer large enough to approximate the behavior of the maximum temperature. Based on prior work, $p = 8$ was chosen [102].

Optimizations were performed on reference channel networks with interior nodal degrees (a measure of redundancy) from 2 – 6 as shown in Fig. 4.2a-e. Nodal degree is defined as the number of channels incident upon any of the four interior nodes. Degree-$n$ designs will henceforth be referred as $D_n$ and reference designs will be referred to as $R$ (see summary of optimization notation in Table 4.2).

During the optimization, interior nodes are restricted to move within the heated zone while corner and edge nodes are restricted to move outside of the heated zone (see Fig. 4.2f-h). To prevent self-crossing of channels, triangles were constructed from the nodes and were restrained to have interior angles $> 10^\circ$ and areas $> 0.001 \times$ the area of the domain (see Fig. 4.2i). At least 48 initial designs were generated for each optimization by shuffling the control points within non-overlapping bounding boxes (see example initial design in Fig. 4.2j).
Fig. 4.2. Reference network configurations and constraints on nodal position. Reference networks are shown for nodal degrees of a) two (D2), b) three (D3), c) four (D4), d) five (D5) and e) six (D6). Bounding boxes are shown for f) interior nodes, g) the top right corner node, and h) the top two edge nodes for the D4 network design. i) Extra nodes and triangles used to prevent self-intersection of channels. j) Example of a randomly generated initial channel design.

Two optimization schemes were implemented which are referred to as $O_0$ and $O_1$ (see Table 4.2). For the $O_0$ scheme, $T_{\text{max}}$ was minimized while the channels were clear. This is the traditional optimization scheme used for battery cooling panels in Chapter 3. For the $O_1$ technique, $T_{\text{max}}$ was minimized while the channel network was subject to its single worst-case blockage (the blockage that gives the highest $T_{\text{max}}$). Panels with blockages were henceforth be denoted as $O_{i(w)}$ and clear panels will be denoted as $O_{i(c)}$ (see Table 4.2).

### Table 4.2. Summary of design notations.

<table>
<thead>
<tr>
<th>Notation</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$R$</td>
<td>Reference design</td>
</tr>
<tr>
<td>$O_0$</td>
<td>Design optimized for clear channels</td>
</tr>
<tr>
<td>$O_1$</td>
<td>Design optimized for a single worst-case blockage</td>
</tr>
<tr>
<td>$O_{i(c)}$</td>
<td>Design operating with clear channels</td>
</tr>
<tr>
<td>$O_{i(w)}$</td>
<td>Design operating with a single worst-case blockage</td>
</tr>
<tr>
<td>$Dn$</td>
<td>Degree-n design</td>
</tr>
</tbody>
</table>
4.2.3. Fabrication of microvascular PDMS panels

Microvascular PDMS panels were prepared using a novel technique combining laser cutting with secondary bonding (Fig. 4.3). Commercial PDMS sheets (McMaster-Carr, part # M8414) of 0.5 mm and 1.6 mm thickness (nominal) were cut into 82 mm x 82 mm squares. A 0.5 mm thick sheet was stacked onto a 1.6 mm thick sheet (Fig. 4.3a) and a 90 W CO$_2$ laser cutter (Full Spectrum Laser, Pro Series, 48” x 36”) was used to cut a path of the desired network into the top piece of PDMS. One pass of the laser was performed at 30% power and 100% speed. The PDMS inside the cut path was manually removed with tweezers (Fig. 4.3b). For some samples, a blockage was introduced into one channel by leaving a 5 mm long uncut piece of PDMS inside. Networks were designed in SolidWorks 2014 and the laser cutter was controlled using RetinaEngrave (2011) software. The average network width was 0.81 mm with a standard deviation of 0.13 mm based on 70 measurements across 5 samples.

Fig. 4.3. Fabrication of microvascular PDMS panels. a) A thin (0.5 mm) PDMS sheet is stacked on top of a thick (1.6 mm) PDMS sheet. b) The top sheet is laser-cut to form a vascular network. c) A thick (1.6 mm) PDMS sheet is bonded to the top using spin-coated Sylgard 184 adhesive. d) The original backing is removed and another thick (1.6 mm) PDMS sheet is bonded in its place. e) Image of microvascular PDMS panel filled with ethanol mixed with Red 3 and Red 40 dyes for visualization. f) Cross-sectional micrograph showing one 0.81 mm x 0.55 mm channel.
Next, Sylgard 184 PDMS adhesive (Dow Corning) was mixed 10:1 base:hardener by hand, degassed for 20 min, and spin-coated (SCS Spin Coater model 6800) onto a 1.6 mm thick sheet of commercial PDMS. Spin coating was performed at 4000 rpm for 200 s to produce a ca. 5 µm adhesive layer. The adhesive-coated PDMS layer was then placed on top of the cut PDMS layer, a weight was added to provide 20 kPa pressure, and the adhesive was cured for 4 h at 80 °C (Fig. 4.3c). After cure, the original 1.6 mm thick PDMS backing was removed and a separate 1.6 mm thick PDMS piece was bonded in its place using the same spin-coating procedure (Fig. 4.3d). Panels were then cut to a final size of 75 mm x 75 mm (see image of a panel in Fig. 4.3e). Final panels were 3.79 ± 0.07 mm thick and contained rectangular microchannels that were 0.81 ± 0.13 mm wide and 0.55 ± 0.03 mm tall (see micrograph in Fig. 4.3f).

4.2.4. Thermal testing of microvascular panels

Needle fittings (20 gauge) were inserted into the inlet and outlet of each panel and sealed by applying epoxy adhesive (Loctite E-120hp) around the perimeter of the panel. One surface of the panel was painted matte black (Krylon) for thermal imaging. Fig. 4.4a shows the experimental setup used for cooling tests. The panel was coupled to a 50 mm x 50 mm polyimide flexible heater (Omega, part # KH-202/10) using thermal grease (Omega, part # Omegatherm 201) and placed on a balsa wood platform. An acrylic plate with a 74 mm x 74 mm viewing window was secured above the top of the specimen to prevent the sample edges from warping upwards during testing (Fig. 4.4b). The heat flux generated by the heater was controlled by adjusting voltage with a Variac variable transformer (Staco Energy Products Co., Type L1010). Voltage values were correlated to heat flux values using the equation \( q'' = \frac{V^2}{RA} \) where \( q'' \) is areal heat flux, \( V \) is voltage, \( R \) is heater resistance, and \( A \) is heater area.

A mixture of 50:50 water:ethylene glycol (Macron Chemicals) was stored in a circulator (Julabo, Model F32-HP) at 21 °C and pumped through the panel with a peristaltic pump (Cole-Parmer Masterflex, Model EW-07551099). Coolant inlet and outlet temperature were measured with thermocouples (Omega part # TMQSS-020U-36, ± 0.5 °C) inserted into the tubing lines. Thermocouple readings were processed with four-port thermocouple readers (Phidgets Inc., model # 1048) and LabVIEW 2013.
Fig. 4.4. Experimental setup. a) Schematic of experimental setup where panel temperature is monitored with an IR camera, coolant temperature is monitored with inlet and outlet thermocouples, and pumping pressure is monitored with a pressure transducer. An acrylic fixture is used to prevent panel out-of-plane warpage. b) Isometric view of panel in acrylic fixture.

Pumping pressure was measured using a wet/wet gage pressure transducer (Omega part # Px26, ± 1 kPa) with a range of 0 – 103 kPa. Transducer readings were processed using a DAQ board (National Instruments, NI USB-6251) and LabVIEW 2013. To account for the pressure drop through the needle fittings, calibration tests were performed in which coolant was pumped through two connected fittings. Final pressure values for a cooling test were found by taking the raw pressure and subtracting the pressure needed to pump coolant through the fittings alone (see Table 4.3 for the pressure adjustment vs. flow rate).

Table 4.3. Average pressure required to pump 50:50 water:ethylene glycol through two 20 gauge needle fittings. Fittings were 12.7 mm long and had an inner diameter of 0.61 mm. Three replicate sets of needles were tested.

<table>
<thead>
<tr>
<th>Flow rate (mL min⁻¹)</th>
<th>ΔP (kPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>1.5</td>
<td>1.3 ± 0.1</td>
</tr>
<tr>
<td>3.5</td>
<td>2.6 ± 0.2</td>
</tr>
<tr>
<td>7.1</td>
<td>5.2 ± 0.3</td>
</tr>
<tr>
<td>14.2</td>
<td>10.8 ± 0.3</td>
</tr>
<tr>
<td>21.2</td>
<td>15.9 ± 0.2</td>
</tr>
<tr>
<td>28.2</td>
<td>23.8 ± 0.5</td>
</tr>
<tr>
<td>42.3</td>
<td>29.2 ± 0.7</td>
</tr>
<tr>
<td>56.4</td>
<td>54.0 ± 1.5</td>
</tr>
</tbody>
</table>
The surface temperature of the panel was recorded with an infrared (IR) camera (FLIR Model SC620, absolute temperature accuracy of ± 2 °C). Experiments were performed to measure panel temperature, coolant temperature rise $\Delta T_c$, and coolant pumping pressure at steady-state. The cooling efficiency $\eta$ of the panel was calculated as the ratio of the heat flux absorbed by the channels $q_c$ to the total heat flux applied $q_t$, i.e.

$$\eta = \frac{q_c}{q_t} = \frac{\dot{m} c_p \Delta T_c}{q_t}$$

(4.1)

where $\dot{m}$ is coolant flow rate and $c_p$ is the specific heat of the coolant.

4.3. Results and discussion

4.3.1. Validation of simulations across varying flow rate

Experiments and simulations were first performed on the reference $D4$ design to validate the IGFEM solver. Simulated and experimental thermal profiles agree well at the baseline flow rate of 28.2 mL min$^{-1}$ as shown in Fig. 4.5. Thermal agreement was maintained from 0 – 56.4 mL min$^{-1}$ flow rate as presented by a plot of $T_{\text{max}}$ in Fig. 4.6a. Maximum temperature equilibrates to ca. 110 °C at zero flow rate, decreases quickly with increasing flow rate, and then plateaus to ca. 60 °C at flow rates above 20 mL min$^{-1}$. Simulated and experimental values of cooling efficiency $\eta$ also agree for different flow rates as shown in Fig. 4.6a. Cooling efficiency increases to ca. 80% and then plateaus above 20 mL min$^{-1}$ flow rate.

![Fig. 4.5. Comparison of thermal profiles for the $D4$ R panel from a) simulation and b) experiment at 28.2 mL min$^{-1}$ flow rate.](image)
Fig. 4.6. Validation of studies for the D4 R panel at different flow rates. a) Experimental $T_{\text{max}}$ and cooling efficiency $\eta$ compared to simulated values. b) Experimental pumping pressure compared to simulated values using both nominal channel dimensions (0.81 mm x 0.55 mm) and dimensions reduced by one standard deviation (0.68 mm x 0.53 mm). Experimental error bars represent the maximum and minimum values for three replicate panels.

Pumping pressure at different flow rates is shown in Fig. 4.6b for simulations using both nominal (average) channel dimensions and channel dimensions reduced by one standard deviation. Pressure rises linearly as expected. The simulations bound the experimental data, with the reduced dimensions providing the closest fit.

4.3.2. Optimization of networks with different nodal degree

Optimizations were first performed to minimize panel temperature while the network was subject to a single worst-case blockage (the $O_1$ scheme). Fig. 4.7 presents optimization results for the 4-degree (D4) reference ($R$). The clear reference panel $R(c)$ has a $T_{\text{max}}$ of 61.7 °C (Fig. 4.7a), which rises to 71.4 °C when blocked (Fig. 4.7b). In contrast, the $O_1$ panel has significantly lower $T_{\text{max}}$ values of 48.4 °C when clear and 59.9 °C when blocked (Fig. 4.7c-d). The blocked $O_1$ panel even outperforms the clear $R$ panel.

The pumping pressures predicted for the $R(c)$, $R(w)$, $O_1(c)$, and $O_1(w)$ panels using reduced channel dimensions are 18.9 kPa, 19.7 kPa, 16.2 kPa, and 16.6 kPa respectively. As expected, the pressure drop increases slightly for both designs when a single blockage occurs.
Fig. 4.7. Improvement of blockage tolerance for the $D4$ panel optimized with the $O_1$ technique. Simulated temperature profiles are shown for a) $R(c)$, b) $R(w)$, c) $O_1(c)$, and d) $O_1(w)$ panels at 28.2 mL min$^{-1}$ flow rate. See Table 4.2 for panel notation. Blocked channels are colored in black.

To support the claim that Fig. 4.7d indeed displays the worst-case blockage for the $D4 O_1$ design, Fig. 4.8 presents the temperature profiles for the blockages that give the six highest $T_{\text{max}}$ values. The very similar $T_{\text{max}}$ values of 59.92, 59.91, 59.78, 59.61, 59.54, and 59.45 °C demonstrate that the optimized design prevents any single blockage location from causing a particularly large temperature rise.

Fig. 4.8. Response of the $D4 O_1$ panel to different blockage locations. a) Simulated temperature profile with the worst-case blockage (same as Fig. 4.7d). b – f) Simulated temperature profiles for the blockage locations that give the next five highest maximum temperatures. The flow rate is 28.2 ml min$^{-1}$ in all cases.

Networks with interior nodal degrees of $2 – 6$ were then optimized for both clear channels ($O_0$) and the worst-case blockage ($O_1$). Fig. 4.9 compares the blockage-tolerance of designs optimized with the $O_0$ and
The two schemes give similar temperature profiles for clear channels (the first and third columns). However, as apparent from the second and fourth columns, the $O_I$ designs perform substantially better than the $O_0$ designs when blocked. The $O_I$ designs particularly outperform the $O_0$ designs at low nodal degree.

The $T_{\text{max}}$ values corresponding to the profiles in Fig. 4.9 are presented in Fig. 4.10. For clear channels, the $O_0$ designs perform better than the $O_I$ designs, which is expected since $O_0$ designs were optimized for this case. The disparity in $T_{\text{max}}$ is moderate for the $D2$ design (a 14 °C disparity) and otherwise relatively small (< 6 °C for all other nodal degrees). For both schemes, increasing nodal degree leads to decreasing $T_{\text{max}}$ due to higher channel density.

For blocked channels, $O_I$ designs outperform the $O_0$ designs for all nodal degree. The improvement in $T_{\text{max}}$ is as high as 19 °C for the $D3$ design. Maximum temperature for $O_I(w)$ decreases steadily with increasing nodal degree due to both higher channel density and the presence of more paths to circumvent blockages. The most blockage-tolerant network, $D6 O_I$, has a $T_{\text{max}}$ of 49 °C when blocked which is only 7 °C higher than when the network is clear. In contrast, the least blockage-tolerant network $D2 O_0$ has a $T_{\text{max}}$ of 90 °C when blocked which is 35 °C higher than when it is clear.
Fig. 4.9. Comparison of panels optimized with the $O_0$ and $O_1$ techniques and nodal degrees of 2 – 6. Simulated thermal profiles are shown for both clear channels (first and third columns) and blocked channels (second and fourth columns) at 28.2 mL min$^{-1}$ flow rate. Blocked channels are colored in black.
Fig. 4.10. $T_{\text{max}}$ as a function of nodal degree for the panels ($O_0$ and $O_1$) shown in Fig. 4.9 for both clear and single blockage scenarios. The flow rate in all cases is 28.2 ml min$^{-1}$.

4.3.3. Validation of optimized designs

Panels were fabricated with the $D2$, $D4$, and $D6$ $O_1$ designs to validate the blockage tolerance of these networks. Tests were performed on panels with both clear channels and the worst-case blockage. Thermal profiles agree well between experiment and simulation for both clear channels (see Fig. 4.11) and blocked channels (see Fig. 4.12). Experiments reveal relatively uniform temperature profiles for clear channels (Fig. 4.11), the formation of hot spots due to blockages (Fig. 4.12), and decreasing panel temperature with increasing nodal degree in all cases.

Simulated and experimental $T_{\text{max}}$ values are plotted vs. nodal degree in Fig. 4.13a. For the $D4$ and $D6$ designs, simulated and experimental $T_{\text{max}}$ values agree within 2 °C which is within measurement error. For the $D2$ design, $T_{\text{max}}$ values agree within 5 °C. The increased disparity is attributed to uncertainty in the convection coefficient $h$, since a single value was chosen for all simulations but $h$ likely increases with increasing panel temperature.

Pumping pressure is compared for the different networks in Fig. 4.13b. Pumping pressure decreases with increasing nodal degree due to shorter channel lengths and lower flow rates per channel. Experimental pressure values are bounded by simulations using average and reduced dimensions for the channel cross-section.
Fig. 4.11. Comparison of simulated and experimental thermal profiles for $D2$, $D4$, and $D6$ \(O_1(c)\) panels at 28.2 mL \(\text{min}^{-1}\) flow rate.

Fig. 4.12. Comparison of simulated and experimental thermal profiles for $D2$, $D4$, and $D6$ \(O_1(w)\) panels at 28.2 mL \(\text{min}^{-1}\) flow rate. Blocked channels are colored in black.
Fig. 4.13. Validation of studies for $O_1(c)$ and $O_1(w)$ panels with different nodal degree. a) Experimental and simulated $T_{\text{max}}$ values vs. nodal degree. b) Experimental pumping pressure compared to simulated values using both nominal channel dimensions of (0.81 mm x 0.55 mm) and dimensions reduced by one standard deviation (0.68 mm x 0.53 mm). Experimental error bars for the $D4O_1(c)$ case represent the maximum and minimum values for three replicate panels. The flow rate in all cases is 28.2 mL min$^{-1}$.

4.4. Summary

A novel gradient-based optimization scheme was developed for use in the optimization of microvascular cooling networks for blockage tolerance. Microvascular PDMS cooling panels were simulated with high speed using reduced-order thermal and hydraulic models and the IGFEM technique. IR imaging of thermal experiments for a reference network design was used to validate the simulations over a range of flow rates. Vascular networks were then optimized by moving channel nodes to minimize the $p$-norm of temperature, a differentiable representation of $T_{\text{max}}$. The optimization was implemented while panels either had clear channels ($O_0$) or the worst-case single blockage ($O_1$).

Optimizations were performed on grid-based networks with interior nodal degree (a measure of redundancy) from 2 – 6. The $O_1$ panels with high nodal degree displayed good tolerance of single blockages. For example, the $D6O_1$ network exhibited a 7 °C rise in $T_{\text{max}}$ when blocked, compared to a 35 °C rise for the $D2O_0$ network. Thermal imaging provided experimental confirmation of blockage tolerance for $O_1$ designs. While this study considered cooling panels, the optimization techniques presented here can be extended to design redundant networks for self-healing and other functions.
CHAPTER 5

ENHANCED CRASHWORTHINESS OF MICROVASCULAR COMPOSITES

5.1. Introduction

Microvascular networks can provide composites with efficient thermal management as demonstrated in the prior three chapters. However, as reviewed in Chapter 1, microchannels have the potential to decrease composite mechanical properties. Prior studies have investigated how channels affect composites in tension, compression, interlaminar fracture, impact, fatigue, and bending [92]. These studies indicate that structural integrity is generally preserved as long as channels are small (ca. < 600 µm) and do not disrupt the angle or continuity or architecture of load-bearing plies. One property that has not been addressed is the energy absorption of composites in a crash (i.e. crashworthiness), an important design factor for their use in transportation.

In this chapter, two different studies are presented on the crashworthiness of microvascular carbon/epoxy composites. First, corrugated cross-ply panels were manufactured containing ca. 400 µm channels at either 10 mm or 1.2 mm interchannel spacing. The 10 mm spacing is representative of typical studies on microvascular composites for self-healing [98] and active cooling [106], while the 1.2 mm spacing is representative of actively cooled composites designed to operate in hypersonic flight [86]. Crush tests were performed for channels oriented either transverse or aligned with the loading direction. The effect of misalignment with the fiber direction of surrounding plies was also investigated. Vascular panels were chamfered and compressed under quasi-static loading to compare failure modes and SEA.

Second, flat (non-corrugated) panels were also tested using a knife-edge fixture. The use of microchannels was investigated to trigger (and guide) failure in a stable fashion to achieve high SEA. Panels were fabricated with one, three or five transverse microchannels located 2 mm from the bottom of the sample, between 0° plies. Channels were either placed directly between 0° plies (scheme A) or the 0°

* This chapter is adapted from an article to be submitted to Composite Structures [148]. 3D-printing was performed in collaboration with graduate student Jia En Aw.
plies were cut to make room for the channels (scheme B). Damage initiation and SEA are compared for samples with no damage trigger, a traditional chamfer trigger, and microchannel triggers.

5.2. Materials and methods

5.2.1. Fabrication of corrugated panels with evenly spaced microchannels

A corrugated panel geometry was chosen to allow for compression without buckling (Fig. 5.1a-b). Dimensions are taken from Grauers et al. [121] and are similar to several other studies on the crashworthiness of corrugated panels [39,122]. Microchannels were incorporated both transverse (Fig. 5.1c) and aligned (Fig. 5.1d) to the load direction. Channels were either 400 µm diameter circular channels at a spacing (s) of 10 mm or 430 µm x 330 µm elliptical channels at s = 1.2 mm. Channel volume fraction was nominally 0.6% for the 10 mm spacing and 4% for the 1.2 mm spacing.

Transverse channels were incorporated in the midplane of a [90|0|3|902]s layup (Fig. 5.2a). Longitudinal channels were incorporated into two different layup sequences to produce channels both aligned and misaligned with the fiber direction of the surrounding plies. Channels misaligned with surrounding plies were created in the midplane of the same [90|0|3|902]s layup (Fig. 5.2b). Channels aligned with surrounding plies were created in the midplane of a [0|90|3|0]s layup (Fig. 5.2c). Note that this case is equivalent to transverse compression of the transverse channel layup (Fig. 5.2a).
Panels were fabricated with twelve layers of unidirectional carbon fiber prepreg. The prepreg consisted of Cycom 977-3 epoxy and Hexcel 12K IM7 carbon fiber (190 g m\(^{-2}\) fiber areal weight, Cytec-Solvay) with a nominal fiber volume fraction of 61%. An aluminum mold with four sets of corrugations was prepared by electrical discharge machining (EDM), and then prepreg was laid over this mold and placed in a vacuum bag using the manufacturer recommended layup (see Fig. 5.3).

The Vaporization of Sacrificial Components (VaSC) technique [67,69] was used to form microchannels with well-controlled interchannel spacing. Sacrificial preforms of polylactide (PLA) infused with 3 wt% tin oxalate (SnOx) catalyst were placed between the 6th and 7th prepreg layers during layup. PLA fibers of
ca. 400 µm diameter (CU Aerospace) were used for \( s = 10 \) mm, while 3D-printed PLA strips of ca. 430 µm x 330 µm cross-section were used for \( s = 1.2 \) mm. The strips were printed using a TAZ6 fused deposition modeling printer (Lulzbot) using PLA filament (CU Aerospace) extruded at 170 °C through a 0.35 mm diameter nozzle at 500 mm/min speed, 0.38 mm print height, and 60 °C bed temperature.

For samples with transverse channels at \( s = 1.2 \) mm, butyl rubber pressure strips (3.2 mm thick, Airtech) were added above the final prepreg layer to provide additional compaction during cure. This step was added since these samples otherwise compacted poorly and contained voids. Laminates were cured in an autoclave at 130 °C for 4 h and 160 °C for 3 h (2 °C min\(^{-1}\) ramps) under 640 kPa external pressure and vacuum (ca. 50 torr). Panels were then cut to size using a diamond saw and VaSC treated at 200 °C for 32 h and vacuum (ca. 1 torr) to vaporize the PLA [67,69].

Channel morphology was characterized using a digital microscope (Keyence VHX-5000). Transverse channels packed well within surrounding fibers and showed dimensional stability (Fig. 5.4a-b). In contrast, longitudinal channels misaligned with surrounding plies were surrounded by a large resin pocket and were significantly compressed during cure (Fig. 5.4c-d). Circular (ca. 400 µm) PLA fibers at \( s = 10 \) mm gave rise to ca. 600 µm x 300 µm channels with discrete resin pockets (Fig. 5.4c), while 3D-printed PLA at \( s = 1.2 \) mm led to ca. 700 µm x 200 µm channels with continuous resin pockets between channels (Fig. 5.4d). Longitudinal channels aligned with surrounding plies had similar morphology to transverse channels (Fig. 5.4e-f).

Table 5.1 presents average channel dimensions for all channel types. Channels aligned with surrounding plies had similar dimensions to the starting PLA materials, while channels misaligned with surrounding plies had significantly altered dimensions as discussed. Variance in channel dimension was similar for channels made with PLA fibers and 3D-printed strips.
Fig. 5.4. Cross-sectional micrographs showing microchannels in corrugated composite panels. Channels were manufactured using either 400 µm PLA fibers at 10 mm spacing (top row) or 430 µm x 330 µm PLA strips at 1.2 mm spacing (bottom row). a - b) Transverse channels (layup in Fig. 5.2a). c – d) Longitudinal channels misaligned with surrounding plies (layup in Fig. 5.2b). e – f) Longitudinal channels aligned with surrounding plies (layup in Fig. 5.2c).

Table 5.1. Average channel width \( w \) and thickness \( t \) for corrugated panels with different layup sequences. The bounds are the standard deviation of measured values. At least seven measurements were taken for each layup.

<table>
<thead>
<tr>
<th>Channel orientation</th>
<th>Alignment with surrounding plies</th>
<th>PLA starting material</th>
<th>( w ) (µm)</th>
<th>( t ) (µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Transverse</td>
<td>Aligned</td>
<td>400 µm fibers(^a)</td>
<td>470 ± 30</td>
<td>410 ± 30</td>
</tr>
<tr>
<td></td>
<td>*</td>
<td>430 µm x 330 µm strips(^b)</td>
<td>460 ± 30</td>
<td>320 ± 10</td>
</tr>
<tr>
<td>Longitudinal</td>
<td>Misaligned</td>
<td>400 µm fibers</td>
<td>570 ± 60</td>
<td>310 ± 20</td>
</tr>
<tr>
<td></td>
<td>*</td>
<td>430 µm x 330 µm strips</td>
<td>690 ± 40</td>
<td>200 ± 20</td>
</tr>
<tr>
<td>Longitudinal</td>
<td>Aligned</td>
<td>400 µm fibers</td>
<td>470 ± 50</td>
<td>420 ± 40</td>
</tr>
<tr>
<td></td>
<td>*</td>
<td>430 µm x 330 µm strips</td>
<td>450 ± 40</td>
<td>340 ± 40</td>
</tr>
</tbody>
</table>

\(^a\) Fibers were nominally 400 µm in diameter. Measured fiber diameter \((N = 10)\) was 430 ± 30 µm.

\(^b\) Measured strip dimensions \((N = 10)\) were 430 ± 20 µm wide by 330 ± 20 µm thick.

5.2.2. Crush testing of corrugated panels

Panels were sanded with a Dremel to provide a 45 ° chamfer for damage triggering (see image of a chamfered panel in Fig. 5.5a). Panels were then compressed at 51 mm min\(^{-1}\) for 40 mm in an electromechanical test frame (Instron Model 5984) with a 150 kN load cell (see setup in Fig. 5.5b). Test videos were recorded with a Canon EOS 7D camera. The specific energy absorbed (SEA) of each
sample is determined by calculating the average load over a specified crush distance, normalized by the mass crushed, i.e.

\[
SEA = \frac{\int_{\delta_1}^{\delta_2} Pd\delta}{m_{\text{crushed}}} = \frac{\bar{P}(\delta_2 - \delta_1)}{L_{c} \cdot \frac{m_{0}}{L_{0}}} \approx \frac{\bar{P}}{m_{0} / L_{0}} \quad (5.1)
\]

where \( P \) is load, \( \delta \) is crosshead displacement, \( \bar{P} \) is the average load in the analyzed displacement range, \( m_{\text{crushed}} \) is the crushed sample mass in the analyzed displacement range, \( L_{c} \) is the crushed sample length, \( m_{0} \) is the initial sample mass, and \( L_{0} \) is the initial sample length. Steady-state crushing was assumed during which \( L_{c} \) is assumed equal to the displacement range \( \delta_2 - \delta_1 \). Values of \( \delta_1 = 10 \) mm and \( \delta_2 = 40 \) mm were chosen to reflect steady-state behavior independent of the trigger process, as described in detail in Appendix A.

\[ \]

**Fig. 5.5.** Crush testing of corrugated panels. a) Image of chamfered sample. b) Panel loaded in compression platens of the Instron test frame.

### 5.2.3. Statistical analysis of SEA data

A Welch’s \( t \)-test [123] was used to evaluate whether SEA values for vascular samples were significantly different than non-vascular samples. The variance of each sample set \( S^2 \) was first calculated as

\[
S^2 = \frac{1}{n-1} \sum_{i=1}^{n} (X_i - \bar{X})^2 \quad (5.2)
\]
where \( n \) is the number of samples, \( X_i \) is the SEA value for a given sample, and \( \bar{X} \) is the average SEA value for the set. The \( t \) value for comparing vascular samples to non-vascular samples was then calculated as

\[
t = \frac{\bar{X}_1 - \bar{X}_2}{\sqrt{\frac{s_1^2}{n_1} + \frac{s_2^2}{n_2}}}
\]  

(5.3)

where the subscripts 1 and 2 denote the non-vascular and vascular sample sets, respectively. The degrees of freedom \( f \) for each comparison were calculated as

\[
f = \frac{(s_1^2 / n_1 + s_2^2 / n_2)^2}{s_1^4/n_1(n_1-1) + s_2^4/n_2(n_2-1)}
\]  

(5.4)

The values of \( t \) and \( f \) for each comparison were used to evaluate a \( p \)-value for a studentized range distribution. Statistical significance was prescribed for \( p < 0.05 \). More details on the studentized distribution can be found in [123].

5.2.4. Fabrication of flat panels with microvascular damage triggers

Flat (non-corrugated) panels were fabricated to investigate the use of microchannels to trigger stable damage modes that lead to high energy absorption. The panel geometry (Fig. 5.6a) consists of 84 mm x 25.4 mm x 3.5 mm samples with loading along the longitudinal (84 mm) axis. The thickness of the panels was chosen to ensure buckling before compressive failure.

Microchannels were incorporated 2 mm from the bottom of each sample using 400 \( \mu \)m diameter PLA fibers. Two incorporation schemes were used deemed scheme A and scheme B. For scheme A, fibers were placed directly between the 0\(^\circ\) plies of a 20-ply [90|0|90|0|90|0|90]\(_s\) layup (see dimensions in Fig. 5.6b and the layup sequence in Fig. 5.6c). Panels were fabricated with three and five channels. The introduction of PLA fibers disrupts the load path of the surrounding 0\(^\circ\) plies, creating fiber waviness and resin pockets. To provide additional space to accommodate the introduction of the PLA fibers, the 90\(^\circ\) plies surrounding PLA fibers were cut to give 1.2 mm gaps (see Fig. 5.7). Gaps were introduced in the middle six 90\(^\circ\) plies for three-channel samples and in the middle eight 90\(^\circ\) plies for five-channel samples.
Fig. 5.6. Geometry, layup sequence, and micrographs of flat microvascular panels for crush testing. a) Panel dimensions (in mm). b) Dimensions and c) [90°/0°/90°/0°/90°/0°], layup for a scheme A sample with five elliptical channels. Samples were also fabricated with the same layup but only the middle three channels. d) Dimensions and e) layup for a scheme B sample with three circular channels. Samples were also fabricated with the same layup but only the middle one or three channels. f–g) Cross-sectional micrographs of three-channel and five-channel scheme A samples. h–i) Cross-sectional micrographs of three-channel and five-channel scheme B samples.

Fig. 5.7. Laminate stacking sequence [90°/0°/90°/0°/90°/0°] for a panel with five microchannels and scheme A. Dimensions are in mm. PLA fibers of 0.4 mm diameter are introduced between 0° plies, and the middle eight 90° plies are given 1.2 mm gaps to provide room for the deformation of 0° plies. Panels were also fabricated with only the middle three channels, for which the middle six 90° plies were given 1.2 mm gaps.
For scheme B, 400 µm PLA fibers were incorporated inside cut gaps within 0° plies (see dimensions in Fig. 5.6d and the layup sequence in Fig. 5.6e). Panels were fabricated with one, three, and five channels located 2 mm from the bottom of the sample. One set of samples was also fabricated with nine channels, in which five channels were located 2 mm from the bottom of the sample, three channels were located 4 mm above the bottom of the sample, and one channel was located 6 mm from the bottom of the sample. The channels were arranged in a diagonal pattern resembling a chamfer (see further discussion of this layup in section 5.3.2).

Panels were fabricated using a flat aluminum mold and the same prepreg as corrugated samples. For scheme A, PLA fibers were pulled through holes in a bracket and held in tension during the cure process to ensure alignment. Laminates were cured with the same cure cycle as corrugated samples, cut to oversized dimensions with a diamond saw, and milled to a 84 mm x 25.4 mm final size. The VaSC process (200 °C and vacuum for 16 h) was then performed to yield ca. 600 µm x 300 µm elliptical channels for scheme A (see Fig. 5.6f-g) and ca. 400 µm diameter circular channels for scheme B (see Fig. 5.6h-i).

5.2.5. Crush testing of flat panels

A knife-edge fixture to support the samples was fabricated based on Ueda et al. [124] and other similar fixtures in the literature [125,126]. The fixture contains knife edges 4.0 mm apart to loosely constrain the 3.5 mm thick samples (Fig. 5.8). A 4.8 mm region beneath the knife edges allows for locally unconstrained sample crushing. Samples were compressed in this fixture at 3 mm min⁻¹ for 15 mm total crush length. SEA was calculated from \( \delta_1 = 5 \) mm and \( \delta_2 = 15 \) mm using equation (5.1).
5.3. Results and discussion

5.3.1. Crush behavior of corrugated panels with evenly spaced microchannels

5.3.1.1. Crush behavior for transverse aligned channels

Crush tests were first performed on non-vascular samples with the same layup sequence as Fig. 5.2a. Non-vascular samples fail through several damage modes as shown in Fig. 5.9a-b. The $0^\circ$ plies in the inside radius of the sample corrugations fail through compressive failure (right side of Fig. 5.9a-b), the middle $90^\circ$ plies fail through compressive failure, and the plies on the outer radius of the sample corrugations fail by splaying (delamination and bending). These failure modes are in agreement with reports in the literature for typical cross-ply carbon/epoxy samples [39,121].

For vascular samples with transverse channels of spacing $s = 10 \text{ mm}$, the failure mode is largely the same as non-vascular samples (Fig. 5.9c-e). However, cracks do initiate at the microchannel locations during crush testing due to stress concentration. The average delamination length for these samples during testing was also 60% higher than non-vascular controls (see Fig. 5.10), which is likely due to the outward pushing of $90^\circ$ ply debris created by channel-initiated cracks. Vascular samples with $s = 1.2 \text{ mm}$ fail similarly to samples with $s = 10 \text{ mm}$ (Fig. 5.9f-g), except that channel-initiated cracks lead to smaller pieces of debris, and the delamination length was not changed from the non-vascular case.
Fig. 5.9. Test results for corrugated panels with transverse channels (layup in Fig. 5.2a) compared to a non-vascular control. Test images are shown for a – b) Non-vascular panel. c – e) Vascular panel with 10 mm channel spacing. A channel-initiated crack is noted in d). f – g) Vascular panel with 1.2 mm channel spacing. h) Representative loading curves. i) Average SEA values. The error bars are maximum/minimum values.

Fig. 5.10. Average delamination length during crushing of corrugated panels. a) Delamination length for panels with transverse channels (layup in Fig. 5.2a) and longitudinal misaligned channels (layup in Fig. 5.2b) at spacings s of 10 mm and 1.2 mm compared to a non-vascular control. b) Delamination length for panels with longitudinal aligned channels (layup in Fig. 5.2c) compared to a non-vascular control. Measurements were taken at three test times (12 s, 24 s, and 36 s) for three samples for each case. The error bars are the standard deviation.
Loading curves for non-vascular samples and vascular samples were similar regardless of channel spacing (Fig. 5.9h). The loading curves feature an initial linear increase in load with the consumption of the chamfer, followed by a region of generally steady crush load. **Fig. 5.9i** compares the average SEA values for these samples. Non-vascular samples have an average SEA of 55 kJ/kg, while vascular samples showed slight reductions in SEA (5% for \( s = 10 \) mm and 10% for \( s = 1.2 \) mm). A two-tailed \( t \)-test reveals that neither of these reductions are statistically significant, with \( p = 0.42 \) for \( s = 10 \) mm and \( p = 0.31 \) for \( s = 1.2 \) mm (see **Table 5.2**).

**Table 5.2.** Average SEA values for all corrugated samples and the \( p \)-values used to assess whether vascular samples had significantly different SEA than non-vascular samples with the same layup sequence.

<table>
<thead>
<tr>
<th>Layup</th>
<th>Sample set</th>
<th>Av. SEA (kJ/kg)</th>
<th>Sample number</th>
<th>( p )</th>
</tr>
</thead>
<tbody>
<tr>
<td>[90][0_3][90_2]_S</td>
<td>Non-vascular</td>
<td>54.5</td>
<td>4</td>
<td>N/A</td>
</tr>
<tr>
<td>*</td>
<td>Transverse ( s = 10 ) mm</td>
<td>51.6</td>
<td>4</td>
<td>0.42</td>
</tr>
<tr>
<td>*</td>
<td>Transverse ( s = 1.2 ) mm</td>
<td>49.3</td>
<td>3</td>
<td>0.31</td>
</tr>
<tr>
<td>*</td>
<td>Longitudinal ( s = 10 ) mm</td>
<td>55.2</td>
<td>3</td>
<td>0.65</td>
</tr>
<tr>
<td>*</td>
<td>Longitudinal ( s = 1.2 ) mm</td>
<td>49.0</td>
<td>3</td>
<td>0.13</td>
</tr>
<tr>
<td>[0][90_3][0_2]_S</td>
<td>Non-vascular</td>
<td>57.7</td>
<td>3</td>
<td>N/A</td>
</tr>
<tr>
<td>*</td>
<td>Longitudinal ( s = 10 ) mm</td>
<td>54.4</td>
<td>3</td>
<td>0.43</td>
</tr>
<tr>
<td>*</td>
<td>Longitudinal ( s = 1.2 ) mm</td>
<td>53.7</td>
<td>3</td>
<td>0.35</td>
</tr>
</tbody>
</table>

5.3.1.2. Crush behavior for longitudinal misaligned channels

Samples were also tested using the same layup sequence, but with misaligned longitudinal channels (Fig. 5.2b). Vascular samples with \( s = 10 \) mm fail qualitatively similarly to non-vascular samples (Fig. 5.11a-d). Vascular samples with \( s = 1.2 \) mm also fail similarly (Fig. 5.11e-f), except with 20% longer delaminations (see Fig. 5.10). The increase in delamination length is likely due to the continuous resin pocket formed in the center of these samples, pushing the surrounding 0° plies farther from the center of the panel and increasing the likelihood of splaying.

Loading curves were similar for all samples (Fig. 5.11g). **Fig. 5.11h** shows that the average SEA for vascular samples with \( s = 10 \) mm exhibits no reduction compared to non-vascular controls. Vascular samples with \( s = 1.2 \) mm showed a 10% reduction, but this was not statistically significant (\( p = 0.13 \), see **Table 5.2**). The preservation of SEA for these samples is attributed to the placement of channels
between the central 90° plies, limiting their influence on the compressive failure of the surrounding 0° plies.

Fig. 5.11. Test results for corrugated panels with longitudinal misaligned channels (layup in Fig. 5.2b) compared to a non-vascular control. Test images are shown for a – b) Non-vascular panel. c – d) Vascular panel with 10 mm channel spacing. e – f) Vascular panel with 1.2 mm channel spacing. g) Representative loading curves and h) average SEA values for these samples. The error bars are maximum/minimum values.

5.3.1.3. Crush behavior for longitudinal aligned channels

Finally, samples were also tested with longitudinal channels in alignment with the surrounding 0° plies (Fig. 5.2c). Non-vascular control samples were tested again since the layup sequence was slightly changed. Again, failure occurs through splaying of the outer plies and compressive failure of the middle 0° and 90° plies (Fig. 5.12a-b). Vascular samples with $s = 10$ mm fail in the same qualitative manner (Fig. 5.12c-d), as do vascular samples with $s = 1.2$ mm (Fig. 5.12e-f).

Loading curves for non-vascular and vascular samples were similar (Fig. 5.12g). Fig. 5.12h shows that vascular samples showed slight reductions in SEA (6% for $s = 10$ mm and 7% for $s = 1.2$ mm) which
were statistically insignificant ($p = 0.43$ and $p = 0.35$ respectively, see Table 5.2). The preservation of SEA is attributed to the fact that these channels are parallel to the load path and do not lead to misalignment of the surrounding 0° plies.

**Fig. 5.12.** Test results for corrugated panels with longitudinal aligned channels (layup in Fig. 5.2c) compared to a non-vascular control. Test images are shown for a – b) Non-vascular panel. c – d) Vascular panel with 10 mm channel spacing. e – f) Vascular panel with 1.2 mm channel spacing. g) Representative loading curves and h) average SEA values for these samples. The error bars are maximum/minimum values.

### 5.3.2. Triggering high energy absorption failure using microchannels

Non-vascular flat panel samples were tested in quasi-static compression using the knife edge fixture shown in Fig. 5.8. Specimens without a chamfer fail by buckling followed by catastrophic splaying failure at the top of the sample (Fig. 5.13a-d). When chamfered, these same specimens fail progressively through the formation of small interlaminar cracks, delaminations, compressive failure of the inner plies, and splaying of outer plies (Fig. 5.13e-g).
Vascular samples made with one, three, or five microchannels and either scheme A or scheme B were tested. For all samples, failure initiated at the microchannels before buckling and splaying. Once triggered, failure progressed through compressive failure of inner plies and splaying of outer plies (Fig. 5.14). The failure modes after triggering were similar regardless of channel number or incorporation scheme.

Fig. 5.15a shows representative loading curves for samples with different types of damage trigger. The sample without a damage trigger (flat edge) reaches the highest load before buckling and splaying occurs. The failure stress is approximately 48% of the predicted compressive failure strength of 870 MPa calculated from Tsai-Wu failure theory and manufacturer ply properties (see Fig. 5.15b). After buckling, the load drops significantly and no further energy absorption occurs. Chamfered samples show a gradual rise to a steady crushing load as the chamfer region is consumed (Fig. 5.15a). The sustained crushing load is primarily attributed mainly to progressive compressive failure of the 0° plies.
Fig. 5.14. Test images showing stable failure of flat panels with microvascular damage triggers. Images are for a – c) three-channel trigger, scheme A, d – f) five-channel trigger, scheme A, g – i) three-channel trigger, scheme B.

Fig. 5.15. Load behavior of flat panels with different damage triggers. a) Representative loading curves for samples with no trigger, a chamfer, and channels made with scheme A and scheme B. Note that the extension axis is zoomed in for the first 1 mm. b) Maximum stress for different damage triggers normalized by the manufacturer compressive failure strength (850 MPa). Error bars are maximum/minimum values for either three samples (for scheme A), four samples (for the flat non-vascular panel and scheme B), or five samples (for the chamfer).

In dramatic contrast, vascular samples show a similar compressive stiffness (compared to no-trigger samples) until reaching a peak load at which point cracks initiate at the microchannels. The load drops
precipitously, but then recovers to a similar sustained crushing load (compared to chamfered samples). For scheme A, the maximum stress values to initiate damage for three-channel and five-channel samples were 43% and 36% of the compressive strength, respectively (Fig. 5.15b). The presence of microchannels disrupts the load path (by ply waviness) to initiate damage before the critical buckling load is reached. Scheme B samples trigger at even lower stress values due to 0° plies being cut instead of just being misaligned. One, three, and five channels led to trigger stresses that were 35%, 19%, and 10% of the manufacturer failure strength. Once triggered, failure is a progressive and stable process that consumes energy.

Average SEA values are presented for different samples in Fig. 5.16. Samples without a trigger have a very low SEA of 2 kJ/kg due to the absence of load recovery after initial splaying. Chamfered and vascular samples show an order of magnitude higher SEA (18 - 22 kJ/kg) due to their sustained crushing load. The lower SEA values for flat panel samples (compared to corrugated samples) is due to the lack of hoop constraint in flat panels, leading to increased splaying [37].

![Graph](image)

**Fig. 5.16.** Average specific energy absorption (SEA) for flat panels with no damage trigger, a chamfer, and channels made with scheme A and scheme B. Error bars are maximum/minimum values for either three samples (for scheme A), four samples (for the flat non-vascular panel and scheme B), or five samples (for the chamfer).

Finally, tests were performed on samples with nine channels arranged in an array resembling a chamfer. **Fig. 5.17a** shows the layup sequence for these samples and **Fig. 5.17b** shows a cross-sectional micrograph of the channels. These samples failed via a diagonal crack through the channels followed by
compressive failure and delaminations (Fig. 5.17c-e). The loading curves reach a trigger load near that of five-channel samples (Fig. 5.17f), with an average trigger stress of $10 \pm 0.4\%$ of the predicted compressive failure strength of 870 MPa ($N = 4$). The average SEA for these samples was $18 \pm 6$ kJ/kg, similar to other vascular samples. The nine-channel samples thus behave very similarly to five-channel samples, suggesting that diagonal cracks and horizontal cracks act as equally effective damage triggers.

Channels were found to be equally effective as chamfers for triggering stable, energy-absorbing damage modes during crushing. Panels with vascular triggers have several advantages over chamfered panels as well. Vascular panels can withstand much higher loads than chamfered panels without triggering damage, and the critical load can be tuned by channel density and/or incorporation scheme. Vascular panels retain flat faces on both ends which are useful for mating components. Finally, channels can be produced for damage triggering in the same step used to produce channels for other functions such as self-healing and active cooling.

5.4. Summary

Two studies were performed on the crashworthiness of microvascular carbon/epoxy composites. Corrugated panels with ca. 400 um diameter microchannels were first tested to study how channel incorporation affected SEA. No significant reduction in SEA was found for any combination of...
interchannel spacing (10 mm or 1.2 mm), channel alignment with the load direction, or channel alignment with the fiber direction of surrounding plies. The preservation of SEA is particularly remarkable for samples with a 1.2 mm channel spacing, which have a channel volume fraction of 4%. In addition, the layup in Fig. 5.2a maintained SEA whether compressed on-axis or off-axis (which is equivalent to crushing the layup in Fig. 5.2c). SEA was preserved for on-axis loading since the microchannels were sequestered within several 90° plies, while SEA was preserved for off-axis loading since the microchannels were aligned with the load direction.

Flat panels were then crushed to show that microchannels can actually enhance crashworthiness by triggering stable, energy-absorbing failure modes. Non-vascular panels without a chamfer buckled, splayed, and had very low SEA. In contrast, panels with microchannels located 2 mm from the bottom of the sample had stable failure and ca. 10 times higher SEA. Vascular damage triggers were also shown to have advantages over chamfer damage triggers, such as higher stiffness and tunable trigger load. These studies confirm that crashworthy microvascular composites can be fabricated for applications such as active cooling and self-healing.
CHAPTER 6
CONCLUSIONS AND FUTURE WORK

6.1. Conclusions

This dissertation reported the exploration of microvascular carbon/epoxy composites as a multifunctional material for EV battery packaging. While actively cooled composites have previously been studied in the literature, this work was the first to show that large-scale (>100 mm x 100 mm in-plane) composite panels can keep a heat source inside a narrow temperature window. Both thermography experiments and CFD simulations confirmed that composite panels can cool EV batteries generating 500 W m$^{-2}$ heat flux below 40 °C. Cooling can be achieved using either straight (1D) channels, which allow for simple manufacturing, or 2D channel networks which give improved thermal performance. Composite panels were also shown to retain their crashworthiness after the introduction of microchannels at several orientations and spacings. This crashworthiness study was the first of its kind and confirms that microvascular composites can simultaneously protect battery cells in a crash and provide active cooling.

More detailed conclusions can be organized into those regarding either straight channels or 2D channel networks. For straight channels, cooling panels were first tested for different values of coolant flow rate, channel density, panel thermal conductivity, and panel thickness. CFD simulations were also performed and accurately captured experimental behavior. Battery cooling was shown feasible for PAN-based carbon/epoxy panels above a threshold flow rate (ca. 20 mL/min) and channel density (16 channels in a 200 mm long panel). A sufficient flow rate is needed to decrease the coolant outlet temperature, while a sufficient channel density is needed to reduce thermal gradients from conduction of heat to channels. Cooling performance was unaffected by panel thickness in the range of 1 mm - 5 mm, suggesting that thinner panels can be used to save weight while thicker panels can be used for increased structural protection. Further improvements in cooling performance were achieved by using pitch-based carbon/epoxy panels, which have much higher in-plane thermal conductivity (ca. 75 W m$^{-1}$ K$^{-1}$) than PAN-based carbon/epoxy (ca. 3 W m$^{-1}$ K$^{-1}$).
Crush tests then demonstrated that straight channels can be integrated into a carbon/epoxy composite without reducing crashworthiness. The best incorporation route was to align channels with the fiber direction of surrounding plies, since this preserves channel shape and the angle of surrounding fibers. The incorporation of aligned channels led to no loss in SEA for corrugated panels, regardless of channel spacing (10 mm or 1.2 mm) or orientation with respect to the load (longitudinal or transverse). The preservation of SEA was either attributed to the channels being surrounded by several non-load-bearing plies (for transverse loading), or to channel alignment with the load direction (for longitudinal loading). In a second study, channels misaligned with surrounding plies were shown capable of triggering energy-absorbing failure modes during compression of flat panels. These combined studies suggest that crashworthy battery packaging can be fabricated by aligning most cooling channels with surrounding plies. Meanwhile, a few misaligned channels can be placed at select locations to act as damage triggers.

Two-dimensional channel networks were also studied and found to outperform straight channels in cooling tests. Carbon/epoxy panels were fabricated with various interconnected 2D channel networks using recent advances in the VaSC technique. The spiral channel network provided the most uniform temperature profile in cooling tests, while the bifurcating design provided slightly reduced thermal performance at much lower pumping pressure (< 10 kPa vs. >100 kPa for the spiral design). Simulated temperature and pressure fields agreed with experimental measurements, confirming that flow through these complex networks can be accurately predicted. Channel size was shown to have little effect on panel temperature, suggesting larger channels can be used to reduce pressure while smaller channels can be used to preserve composite mechanical properties.

Two-dimensional cooling networks were finally optimized to reduce panel temperature in a collaborative effort. Carbon/epoxy panels with parallel channel networks were first optimized to reduce $T_{\text{max}}$ by changing the location of channel nodes. Experiments confirmed that the optimization process can eliminate local hot spots in a cooling panel. Optimizations were then performed to optimize the blockage tolerance of 2D cooling networks in PDMS panels. Experiments confirmed that blockages lead to much smaller temperature rises for optimized networks compared to reference networks. Blockage tolerance also increased with higher nodal degree (a measure of redundancy), as seen in natural vascular systems. These optimization techniques are very flexible, allowing for the optimization of battery cooling panels for
any desired combination of panel dimensions, uniform or non-uniform applied heat flux, and available coolant flow rate and pumping pressure.

6.2. Future work

6.2.1. Manufacturing of microvascular composites

The manufacturing of composite vasculature can be improved in several ways. While straight (1D) channels can be readily integrated into composites without disrupting fiber architecture, no techniques have been developed for seamlessly integrating 2D and 3D interconnected channel networks into composites. One approach for 3D integration is to weave PLA fibers directly into reinforcement fabric. Weaving studies should be performed to demonstrate that PLA fibers can be woven into 3D, interconnected paths within fabrics to form 3D channel networks after VaSC. This technique shows promise since Esser-Kahn et al. [67] already demonstrated that PLA fibers can be woven into fabric preforms to produce isolated channels.

For prepreg composites, an ongoing study has shown that PLA fibers can be directly integrated into plies during prepreg manufacturing [127]. This prepreg has been used to form three-dimensional channel networks by consolidating PLA fibers together during cure. Further work is needed to improve the consistency of interconnects and to characterize flow through these networks. Finally, 3D-printing has recently emerged as a viable manufacturing technique for short-fiber and continuous fiber composites [128]. Sacrificial fibers could potentially be printed directly within a reinforcement phase to form complex channel structures.

The standard VaSC procedure for PLA with SnOx catalyst (200 ºC for ≥ 12 hr) also adds considerable fabrication time. There is a need for sacrificial materials that degrade faster, such as PLA with an improved catalyst or metastable polymers such as poly(phthalaldehyde) [129] or poly(vinyl butyl carbonate sulfone) [130]. The key challenge is to find a material that is processable, available in bulk quantities, and stable during initial composite cure. The ideal sacrificial material would simply degrade by the end of the composite cure cycle, requiring no secondary treatment. This approach may be achieved with frontal polymerization of matrix material, which provides high-speed, high-temperature cure [131].
Quickly degrading sacrificial materials should finally be developed for thermoplastic composites, since thermoplastic composites are fast to manufacture, recyclable, and have high crashworthiness suitable for automobiles [132]. The challenge is to find a material that is stable at the temperatures used in melt processing (e.g. 270 °C for nylon 66 [133]) but can then be triggered to degrade at lower temperature. Metastable polymers with endcaps that can be triggered by non-thermal methods such as UV light or pH are a promising solution [134].

6.2.2. Multifunctional fluids for vascular materials

Many functions have been achieved in microvascular composites through the use of different fluids such as coolants, healing agents, liquid metals, and ferrofluids [67,70,43,79]. However, a more elegant approach is to use a single fluid for multiple functions as seen in nature. For instance, mammalian blood simultaneously provides thermal regulation, damage sensing, clotting and healing, nutrient/waste transport, and immune function [55]. These functions are achieved with low-viscosity plasma that contains many functional particulates such as red blood cells, white blood cells, and platelets. Pumping pressure is regulated both by low fluid viscosity and by vasodilation of blood vessels [55].

A promising target for a multifunctional vascular fluid is combined healing and cooling. The challenge is to find a low viscosity fluidic healing agent that also has good coolant properties (high thermal conductivity and specific heat). The epoxies used in most self-healing studies are unfeasible for cooling due to their high viscosity (1000 – 6000 cP) compared to that of typical coolants (1 – 10 cP) [135,136]. Dicyclopentadiene (DCPD) is a promising fluid candidate, since it has low viscosity (0.7 cP [137]) and can heal cracks in polymeric systems upon contact with Grubb’s catalyst [56]. The specific heat of DCPD (2.0 J g⁻¹ K⁻¹ [137]) is lower than traditional coolants such as water/ethylene glycol (3.5 J g⁻¹ K⁻¹), but this can be offset by using high flow rates to reduce coolant temperature rise. The thermal conductivity of DCPD is also relatively low (0.15 W m⁻¹ K⁻¹ [137] vs. 0.42 W m⁻¹ K⁻¹ for water/ethylene glycol), but this can be offset by using high channel density to reduce thermal gradients within channels. Experiments should be performed to confirm that DCPD can adequately cool composite panels. Cracks should then be introduced that intersect the channels, to see if DCPD can heal the cracks without clogging the channels.
Finally, flow battery anolyte and catholyte solutions could be used to provide a composite with simultaneous energy storage and cooling. Solutions would be pumped through two channel networks separated by a microporous membrane for ion flow. This concept was explored in [138,139], where a microporous polyimide battery separator was formed through the VaSC of a PI/PLA blend. The PI/PLA blend could potentially be coated between PLA fibers, giving rise to channels connected by a porous film after VaSC. Future work should realize this separator fabrication process in a fiber-reinforced composite. The next step would be to show that carbon fiber surrounding the channels can be used to conduct electricity and complete the battery circuit.

6.2.3. Effect of 2D channel networks on composite mechanical properties

Several mechanical studies have been performed on composites with straight channels as reviewed in Chapter 1. However, no studies have investigated how 2D channel networks affect composite properties. Mechanical tests should be performed on composites with 2D networks to measure key properties such as tensile modulus/strength, compressive modulus/strength, fracture toughness, and crashworthiness. Variables of interest include channel size, network nodal degree, and channel shape (e.g. circular channels made from 3D printed PLA vs. rectangular channels made from laser-cut PLA).

For unidirectional prepreg composites, 2D networks will likely cause larger changes in mechanical properties than straight channels, since 2D networks cannot be incorporated such that all channels are aligned with surrounding plies. In contrast, 2D networks are expected to have less impact on mechanical properties in fabric composites since the fabric can better conform over the network (see Chapter 3). Regardless of incorporation scheme, 2D networks are also expected to cause heightened stress concentrations at channel intersections. For example, the theoretical stress concentration factor for two circular channels intersecting at a right angle is $K_t = 4.7$, compared to $K_t = 3$ for a single channel [140]. Rectangular channels are also expected to provide higher stress concentration factors than circular channels, since rectangular channels feature corners with radii of curvature smaller than the channel height and width (see [141]).
6.2.4. Crashworthiness of microvascular composites

Full-speed crush tests (ca. 30 km h\(^{-1}\)) should be performed on microvascular composites to confirm the quasi-static results presented in Chapter 5. Full-speed tests are expected to give similar SEA values, since carbon/epoxy composites are typically strain rate insensitive for these test speeds [142]. Next, full-speed tests should be performed where channels are filled with coolant to better represent how composites would fail in operation. Interestingly, compressive stiffness and energy absorption could increase when the channels are filled with an incompressible fluid. The fluid may not have sufficient time to flow when compressed, causing it to pressurize and act as a stiff insert.

Vessels filled with incompressible fluid are actually used to enhance energy absorption in many natural systems. When articular cartilage (cartilage between bones) is impacted, pockets of interstitial fluid are pressurized which can contribute to over 90% of the cartilage's load-bearing ability [143]. Similar pockets of interstitial fluid contribute to added stiffness in mammal paw pads [144] and the hydrostatic skeletons found in many invertebrates [145].

Crush tests should be performed for different channel architectures to see if improvements similar to those in nature can be achieved. Panels containing channels longitudinal to the load direction should first be tested, since this orientation will lead to more direct fluid pressurization. Different channel volume fractions (e.g. 5%, 10%, 15%) should be tested to find the threshold at which the fluid carries a significant portion of the load. A key challenge will be how to seal the fluid inside the channels to prevent any pressure loss. Different crush speeds should also be implemented to find the threshold at which fluid pressurization becomes significant. If fluid-filled longitudinal channels are shown to enhance crashworthiness, tests should be repeated for transverse channels at various volume fractions.
REFERENCES


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APPENDIX A
ANALYSIS OF CORRUGATED SAMPLE LOADING CURVES

As described in Chapter 5, specific energy absorbed (SEA) values were calculated for corrugated crush samples using the equation

\[
\text{SEA} = \frac{\int_{\delta_1}^{\delta_2} P \delta \, d\delta}{m_{\text{crushed}}} = \frac{\bar{P}(\delta_2 - \delta_1)}{L_c \cdot m_0 / L_0} \approx \frac{\bar{P}}{m_0 / L_0}
\]

(A.1)

where \( P \) is load, \( \delta \) is crosshead displacement, \( \bar{P} \) is the average load in the analyzed displacement range, \( m_{\text{crushed}} \) is the crushed sample mass in the analyzed displacement range, \( L_c \) is the crushed sample length, \( m_0 \) is the initial sample mass, and \( L_0 \) is the initial sample length. Steady-state crush behavior was assumed during which \( L_c \) is assumed equal to the displacement range \( \delta_2 - \delta_1 \). Values of \( \delta_1 = 10 \text{ mm} \) and \( \delta_2 = 40 \text{ mm} \) were chosen based on analyzing sample loading curves as follows.

Fig. A.1 shows a representative loading curve for a sample with transverse channels at 1.2 mm spacing. The load curve features an initial rise until \( \delta \approx 3.5 \text{ mm} \), a general decline until \( \delta \approx 10 \text{ mm} \), and then a relatively steady load from \( \delta \approx 10 \text{ mm} \) to \( \delta = 40 \text{ mm} \). Similar load behavior was seen for other corrugated samples.

The initial rise in load corresponds to the consumption of the chamfer (Fig. A.2a-b). Once the chamfer is consumed, delaminations steadily increase in length while the load decreases (Fig. A.2c-d). The delaminations then reach a stable length for the rest of the test, during which the load is roughly constant (Fig. A.2e).
Fig. A.1. Representative loading curve for a corrugated crush sample with transverse channels at 1.2 mm spacing.

The loading curve in Fig. A.1 was integrated to create a plot of total energy absorbed vs. displacement (Fig. A.3a). The slope of this curve rises with the consumption of the chamfer, falls with the decrease in load after chamfer consumption, and then reaches a roughly stable value after $\delta \approx 10$ mm. If energy absorbed is plotted for only the $\delta = 10$ mm to $\delta = 40$ mm range, the slope is seen to be almost perfectly linear (Fig. A.3b). This suggests that the $\delta = 10$ mm to $\delta = 40$ mm range is appropriate for assuming steady-state crush behavior.
Fig. A.3. Total energy absorbed vs. displacement for the loading curve in Fig. A.1. Data is shown for a) the full displacement range and b) the $\delta = 10$ mm to $\delta = 40$ mm range. Linear trendlines are shown in red.

SEA values were finally calculated for different displacement ranges to further validate that steady-state behavior is obtained after $\delta \approx 10$ mm. Fig. A.4 presents SEA values over 5 mm displacement increments for the loading curve in Fig. A.1. SEA rises initially but then plateaus after $\delta = 10$ mm.

Fig. A.4. SEA values over 5 mm displacement increments for the loading curve in Fig. A.1.