Inertial stabilization of buckling at high rates of loading and low test temperatures: Implications for dynamic crush resistance of aluminum-alloy-based sandwich plates with lattice core

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Abstract

In the present study, the dynamic compressive behavior of aluminum-alloy-based tetrahedral-core truss structures is investigated as a function of impact velocity using a split Hopkinson pressure bar. The results are used to understand the phenomenon of buckling in truss structures as a function of loading rates and test temperatures, and its implication on the dynamic crush resistance of tetrahedral-truss-based sandwich structures. In order to understand the effects of the flow strength of the core on the dynamic crush resistance, truss structures of both T6 and OA heat-treatments of AA6061 were investigated. A high-speed digital camera was used to record the sequence of the deformation and failure events that occur during the dynamic compression and failure of the truss sub-elements. A buckling instability was observed to occur consistently for both the T6 and OA microstructures at all test temperatures employed in the present study. Moreover, the T6 heat-treatment and the lower-than-room test temperatures significantly increase the specific energy absorption capabilities of the truss structures.

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1. Introduction

The response of monolithic beams and plates to impact loading has been extensively investigated in the past. For example, Seiler and Symonds [15] and Wang and Hopkins [23] have analyzed the impulsive response of beams and clamped circular plates, respectively. More recently, advances in material fabrication and processing have led to the emergence of a new class of ultra-lightweight, high-energy-absorption cellular material systems. Examples of such systems include metal foams and truss core lattice structures. In particular, Wadley et al. introduced a new fabrication methodology to fabricate metal-based open-cell tetrahedral truss core structures from aluminum alloy sheets [16,22,9]. These cores are made by folding stretched hexagonally perforated Al-6061 sheets. Simple air-brazing is then used to construct sandwich panels with cellular cores with relative densities between 0.02 and 0.08.

Research on periodic cellular materials has been widely reported in the context of minimum weight design, advances in manufacturing, mechanical characterization, structural integrity and large-scale simulations. Amongst these efforts, experimental studies providing a fundamental understanding of the mechanical behavior of these materials have been of particular importance. Quasi-static tests have been accurately reconciled with models of metal-based foams that account for ligament bending. Those
for periodic truss-element cellular cores have addressed plastic yielding and inelastic truss buckling. However, it is now understood that the dynamic response of these materials is significantly different from that measured under quasi-static conditions. Besides material strain rate sensitivity [14, 6, 2, 3, 27, 17, 28], two other dynamic effects [21, 26] are understood to significantly affect the core behavior: (i) inertial resistance of the core to the motion of the front face-sheet and the consequent plastic-wave propagation in the core; and (ii) inertial stabilization of the core ligaments that can delay the onset of buckling, thereby maintaining the effective strength of the core to much larger crushing strains than under quasi-static crushing. Core ligament stabilization is also understood to lead to significant increase in the energy-absorption capacity of the lattice core sandwich plates.

In some of the earlier studies on cellular materials, Bart-Smith et al. [1] reported both the measured and simulated bending performance of sandwich panels with thin cellular metal cores (an Alporas core and two Al alloy face sheets). Using collapse load criteria for face yielding, core shear and indentation, they developed a mechanism map that characterized the observed predominant failure phenomena. Xue and Hutchinson [25] compared the performance of metal sandwich plates and solid plates made from the same material and mass under impulsive blast loads. In their study, three core geometries were evaluated: pyramidal truss, square honeycomb and the folded plate. They reported that sandwich plates made from these three core geometries can sustain a much larger blast loading when compared to a solid plate of equal mass; the square honeycomb and folded plate cores were consistently observed to outperform the truss cores. They also proposed the possibility of using structural optimization schemes that could possibly lead to even greater improvements in the performance of the sandwich plates. Wicks and Hutchinson [24] examined the mechanical performance of sandwich plates with truss cores. They found the truss core construction to be as efficient as the honeycomb cores at carrying the prescribed combinations of moment and transverse forces when a realistic minimum crushing strength was imposed. Truss cores were found to possess an inherent crushing advantage at the low core densities typical of most sandwich plate designs. For the case of woven textile cores, Lee et al. [10] presented the compressive behavior of 304 stainless steel woven textile core materials obtained over a wide regime of deformation rates. A well-defined failure mode transition was observed when the deformation rate was increased from $500 \text{ s}^{-1}$ to $1 \times 10^4 \text{ s}^{-1}$, and the peak stress was found to be deformation rate sensitive. At low strain-rates, the collapse zone was observed to be in the central region, with shear bands at $\pm 45^\circ$ in orientation, while at the high strain-rates the collapse occurred adjacent to the impact surface and propagated through the specimen in the direction of impact. The overall dynamic response of the textile cores was classified to be in between the open-cell foams and the pyramidal truss core cellular materials.

Following these early ideas, Qiu et al. [11, 12] conducted extensive research involving structural optimization and developed analytic approximations for the behavior of sandwich panels subjected to blast loading. Valdevit et al. [20] evaluated the optimal dimensions and the minimum weight of sandwich panels with prismatic cores. They reported that the corrugated core sandwich panels performed best when loaded longitudinally, and attributed this to their greater buckling resistance; the diamond prismatic core panels (with corrugation order $= 4$) were found to be slightly more weight efficient when compared to the truss, when optimized for a specific loading direction. Rathbun et al. [13], using a shock simulation technique involving high-speed impact of Al-foam projectile, studied and compared the performance of stainless steel square honeycomb core sandwich and solid monolithic beams subjected to high-pressure, short-duration impulses. These studies confirmed that sandwich beams with square honeycomb cores demonstrated considerably smaller displacements when compared to the solid steel beams of equal mass when subjected to impulse/shock loading. In particular, for low-amplitude impulses, i.e. when the core has sufficient dynamic strength to prevent appreciable crushing, the benefits of the sandwich structure were particularly large. Hutchinson and Xue [7] analyzed square honeycomb core plates for the best achievable performance and for optimal mass distribution between the face plates and the cores. The model was also used to discuss a number of issues related to the design of effective metal sandwich plates, including differing requirements for air and water environments, face-sheet shear-off resistance, the role of core strength and the relation between small-scale tests and full-scale behavior.

From these studies it is now understood that all-metal sandwich plates have distinct advantages over monolithic plates of equal mass for structures designed to withstand intense short-duration pressure pulses, especially in water environments. To be effective under intense impulsive loads, a sandwich plate must be able to dissipate, via core crushing, a significant fraction of the kinetic energy initially acquired. Consequently, if a plate is to retain its integrity with only limited crushing, its core must have ample crushing strength and high energy-absorbing capacity. The higher core strength is expected to limit core crushing, enable high specific energy absorption, and maintain bending strength of the plate; sandwich plates with weaker cores require a much higher fraction of their mass in their core.

The objective of the present paper is to better understand inertial stabilization of buckling as a function of loading rates and the material yield strength and its role in controlling the crush resistance of aluminum-alloy-based sandwich structures (Fig. 1). Both T6 and over-aged (OA) of AA6061, which are known to have very different yield and flow strengths under dynamic compression [18], were utilized. Moreover, the test temperatures were varied from room to lower-than-room temperature (down to $-170^\circ\text{C}$). The focus of our study was the tetrahedral truss core
because it has been shown to have a relatively high specific crushing strength and energy absorption capacity that is essential if the sandwich plate is to outperform the solid plate construction. In order to conduct the dynamic experiments, the AA6061 alloy truss sub-elements, in the two heat-treatments, were dynamically deformed in compression using the split Hopkinson pressure bar (SHPB) facility in the Department of Mechanical and Aerospace Engineering at Case Western Reserve University. Moreover, a modified SHPB facility, which incorporates a low-temperature chamber, was used to conduct the lower-than-room test temperature experiments. The results of these experiments were used to examine the effects of the changes in yield strength and flow behavior of the lattice core material on inertial stabilization of the truss ligaments, and hence the specific energy absorption and crush resistance of the tetrahedral core sub-elements.

2. Experimental procedure

2.1. Materials

In the present study, the AA6061 alloy sandwich panels were acquired as 4 in. square plates from the University of Virginia. Age-hardenable Al–Mg–Si aluminum alloy (6061) tetrahedral lattice truss sandwich panels were made using a recently developed sheet folding process [16]. The lattices were brazed to solid face sheets of similar alloy composition and tested in both the annealed and peak age-hardened conditions. Details of the fabrication of the tetrahedral lattice sandwich panels are provided next.

2.1.1. Tetrahedral lattice truss and sandwich panel fabrication

A folding process was used to bend elongated hexagonal perforated AA6061 (Al–0.6Si–1.0Mg–0.28Cu–0.20Cr wt.%) sheet to create a single layer 50% occupancy tetrahedral truss lattice [9]. Fig. 1 schematically shows the process. The folding was accomplished node row by node row using a paired punch and die tool with the sheets folded so as to form regular tetrahedrons (i.e. the angle $\theta = 54.7^\circ$).

An example of an elongated hexagonal perforated sheet (with open area fraction of 0.82) that would create a tetrahedral lattice with predicted relative density, $\bar{\rho} = 0.067$ is shown in Fig. 2a. Fig. 2b shows a folded tetrahedral lattice truss with a (measured) pre braze relative density, $\bar{\rho} = 0.048$. The relative density of the truss cores was varied by using different perforated sheet thicknesses and appropriately spacing the perforating punches to maintain a square truss cross section. This required only one punch/die set to produce the five relative density lattices investigated.

Sandwich panels were constructed from the folded truss structures by placing a tetrahedral lattice core between AA6951 alloy sheets clad with AA4343 braze alloy. Alloy compositions, melting and brazing temperatures are shown in Table 1. The assembly was then coated with a proprietary metal-halide flux slurry (Handy Flo-X5518, Lucas Milhaupt Inc., Cudahy, WI), dried and placed in a muffle furnace for brazing. Each assembly was heated to between 595 ± 5°C for approximately 5–10 ± 1 min (depending on sandwich mass) to minimize the joint weakening associated with silicon interdiffusion from the brazing alloy. After

![Fig. 1. Schematic of the manufacturing process of the tetrahedral lattice truss cores involving perforation and folding.](image1)

![Fig. 2. (a) Photographs of the perforated sheet used to form a 4.8% relative density core and (b) the corresponding tetrahedral lattice truss after the folding operation.](image2)
air-cooling to ambient temperature, one set of sandwich panels was solutionized at 530 °C for 60 min and subsequently furnace cooled to place the alloy in the annealed (O) condition. A second set of panels was water quenched from the solutionizing temperature and then aged at 165 °C for 19 h. This achieved the peak strength (T6 temper) for the AA 6061 alloy. No visible distortion was observed after water quenching. Tensile test coupons of AA6061 accompanied the cores through each thermal process step and were later used to approximate the mechanical properties of the parent material used in the tetrahedral lattice trusses. Fig. 3 shows the schematic laboratory brazing/heat-treatment process flow. Two orthogonal views of a sandwich panel with a post braze 3.0% relative density core are shown in Fig. 4. The brazing step resulted in an increase in the relative density of up to 0.8% and depended on the AA4343 clad thickness.

For the purposes of the present study, two different heat treatments, i.e. T6 and over-aged (OA), were selected for the AA6061 truss structures. The as-received AA6061-alloy-based truss structure was in the T6 heat-treatment condition. It was subsequently exposed to 260 °C for 10 h, followed by air cooling (24 °C) at room temperature to obtain the OA condition. High strain-rate tension and compression studies on monolithic AA6061 alloy, in both the T6 and OA heat treatments and as a function of test temperatures, have been previously conducted by the authors [18,19]. The results of the study are summarized in Table 2. It provides the uniaxial compression flow stress of both AA6061-T6 and AA6061-OA alloys at a true strain of 0.1, as a function of both the strain-rate and test temperatures.

For dynamic compression testing, identically sized truss sub-elements were electro-discharge machined with square-edge length of 16 mm (0.63 in.) and height 15 mm (0.59 in.), as shown in Fig. 5. The relative density, $\bar{\rho}$, of the present tetrahedral truss core, which is defined as the ratio of the core density to the parent material density, is given by Kooistra et al. [9]

$$\bar{\rho} = \frac{2}{\sqrt{3}} \cos^2 \theta \sin \theta \left(\frac{t}{l}\right)^2.$$  

Fig. 3. Furnace brazing and heat treatment process used to create AA6061 tetrahedral lattice truss sandwich panels.
In Eq. (1) $\theta$ is the angle between each tetrahedral strut and the corresponding base tetrahedron, and $t$ and $l$ are the tetrahedral strut thickness and length, respectively. Considering the topology properties of a regular tetrahedron, $\theta = \arctan(\sqrt{2}) = 54.7^\circ$. For the sample used in present study, $t$ and $l$ are 1.5 mm (0.059 in.) and 13.4 mm (0.53 in.), respectively. Thus, the relative density, $\rho$, for the truss core used in the present investigation is 0.0524.

### 2.2. Dynamic compression testing: the split Hopkinson pressure bar (SHPB)

The high-strain-rate dynamic compression experiments on the aluminum-alloy-based tetrahedral sub-elements (shown in Fig. 5) were conducted using the SHPB facility in the Department of Mechanical and Aerospace Engineering at Case Western Reserve University. The schematic of the SHPB facility is shown in Fig. 6. The SHPB comprises a striker bar, an incident bar and a transmitter bar, all made from high-strength maraging steel with nominal yield strength of 2.2 GPa. The striker bar used in the experiments was approximately 0.6 m long, while the incident and transmitter bars were 1.524 m in length. The diameter of all the pressure bars was 19.05 mm (0.75 in.). A pair of semiconductor strain gages (BLH SPB3-18-100-U1) were strategically attached on the incident and transmitter bars;
these gages were used in the combination with a Wheatstone bridge circuit, connected to a differential amplifier (Tektronix 5A22N) and a digital oscilloscope (Tektronix TDS 420) to record the strain-gage profiles in the incident and the transmitter bars during the test.

In order to conduct the experiments, the truss sub-element specimen was placed between the incident and transmitter bars, as shown schematically in Fig. 7. In all experiments, the contacting surfaces of the sub-element were polished with ultra-fine sandpaper (up to 2400 grit). Moreover, molybdenum-disulfide grease was used between the sub-element surfaces and the incident and transmitter bars to minimize friction.

An IMACON 200 high-speed digital camera, with a maximum framing rate of 200 million frames per second, was used to monitor the dynamic deformation process. The camera allows a maximum of 16 frames of a typical short-duration event to be recorded. The framing rate is adjustable so as to allow for the best coverage of the entire event. For simple visual imagery, the high-speed camera uses a Photogenic Power Light 2500 DR flash, which illuminates the test sample (sub-element) with \( \frac{1}{24} \) kJ of light energy within a span of about 200 µs. The camera is configured with appropriate Nikon lenses for the best magnification and visibility, and is placed at a suitable distance from the test sample for the required magnification.

In order to conduct the experiments on the truss sub-elements, the striker bar is accelerated by means of a compressed-air gas-gun to impact the incident bar; the impact results in an elastic compression wave, with a strain profile \( \varepsilon_i(t) \), traveling along the incident bar towards the sub-element sample. Because of the impedance mismatch between the sub-element and the pressure bars, part of the wave, \( \varepsilon_r(t) \), is reflected back into the incident bar, and the rest, \( \varepsilon_t(t) \), is transmitted through the sub-element specimen into the transmitter bar. Based on the one-dimensional elastic wave-propagation and SHPB theory [4,5] the engineering stress \( \sigma(t) \), engineering strain-rate \( \dot{\varepsilon}(t) \), and the engineering strain \( \varepsilon(t) \) in the sub-element can be calculated:

\[
\sigma(t) = E \frac{A_0}{A_s} \varepsilon_i(t), \tag{2}
\]

\[
\dot{\varepsilon}(t) = -2 \frac{c_0}{L_s} \varepsilon_r(t), \tag{3}
\]

\[
\varepsilon(t) = \int_0^t \dot{\varepsilon}(t) \, dt. \tag{4}
\]

In Eqs. (2)–(4), \( E \), \( A_0 \) and \( c_0 \) are the Young’s modulus, cross-sectional area and longitudinal wave speed of the pressure bars, and \( A_s \) and \( L_s \) are the initial contact cross-sectional area and height of the sub-element tested.

Due to the anisotropic topology of the truss structure, the true-stress vs. true-strain profile in the sub-element is difficult to estimate, since in the analysis of the traditional SHPB tests on isotropic and homogeneous specimens, isochoric and uniform deformation conditions are assumed to prevail within the specimen [4,5]. Instead, a force vs. displacement curve for the truss sub-element sample is estimated by multiplying the nominal engineering stress with the original sub-element contact area, while the displacement of the sub-element during dynamic compression was calculated by multiplying the nominal engineering strain by the original specimen height.

To conduct the low-temperature SHPB tests, a simple lightweight plastic foam cooling tank, shown in Fig. 8, was designed to keep the specimen immersed in liquid nitrogen (at \(-196 °C\)) prior to the dynamic compression testing. To accommodate the incident and the transmitter bars within the cooling tank, two holes, with diameters close to those of the incident and transmitter pressure bars,
were drilled on the opposite curved surfaces of the tank. Prior to impact, the sub-element sample was clamped at the ends of the incident and transmitter bars within the foam cooling tank. Liquid nitrogen was then poured into the cooling tank to fully immerse the specimen as well as the ends of the pressure bars. The temperature of the sub-element was monitored by a 0.015 in. chromel–alumel thermal couple wire attached to the sub-element.

3. Experimental results and discussion

Table 3 provides a summary of all the experiments conducted in the present study. It provides the test number, the heat-treatment of the aluminum alloy used for the truss sub-element, the projectile bar velocity and the test temperature. In order to compare the dynamic deformation characteristics of the truss sub-elements at the room and −170 °C test temperatures, the impact velocity of the striker bar was controlled to be approximately 20 m s⁻¹. This corresponds to an input stress level of ~400 MPa. For all experiments the length of the striker bar was fixed at 0.6 m (~24 in.). This corresponds to a input pulse duration of ~250 μs. For Test 4 an impact velocity of 11.3 m s⁻¹ was used. The motivation using the lower impact velocity was to deform the truss sub-element to an intermediate stage, such that it is pushed into the post-buckled regime but not completely crushed. Post-test examination of the truss core was then used to elucidate the modes and sequence of events that lead of failure, e.g. initiation of plastic buckling instability, role of micro-inertia, etc., in the truss elements prior to complete collapse.

The force vs. displacement profiles obtained during dynamic crushing of the AA6061-T6 and AA6061-OA sub-elements at both room and lower-than-room test temperatures are shown in Fig. 9. The oscillations in the force-displacement curves are understood to be a consequence of stress wave dispersion in the truss sub-element and also due to the complex load transfer path changes during collapse of the truss core structure during dynamic compression. The average strain-rate, estimated from the transmitter bar gage signals, was ~1000 s⁻¹. However, for Test 4, the nominal strain rate was ~500 s⁻¹. This is because a lower impact velocity was used in this experiment. In both the room temperature and lower-than-room test temperature tests, the AA6061-T6 sub-elements exhibited higher compressive strength and consistently absorbed higher impact energy when compared to the AA6061-OA alloy structures. This behavior is consistent with the dynamic yield strength and flow-stress reported for the two alloys on monolithic cylindrical compression specimens [18,19], as discussed in Table 3.

In addition to obtaining the force-displacement curves, in some of the room temperature tests the IMACON 200 high-speed camera was utilized to observe the deformation and failure process of the truss sub-elements. Analysis carried out by Xue and Hutchinson [26] and Vaughn et al. [21]
have shown that strong dynamic effects come into play when sandwich plates are subject to high-intensity stress pulses. They introduced an important dimensionless parameter governing the inertial effects, \( \frac{V_o}{(c_o \varepsilon_Y)} \), where \( V_o \) is the relative velocity of the sandwich faces, \( c_o = \sqrt{E/\rho} \) is the elastic wave speed in the truss core material, and \( E, \rho \) and \( \varepsilon_Y \) are the Young’s modulus, density and the initial yield strain of the metal ligaments, respectively.

To gain an appreciation of the timescales involved in the experiments discussed here, consider the AA6061-T6 truss sub-element with a core thickness of \( h = 15 \text{ mm} \), whose front face is abruptly accelerated to a velocity of \( V_o = 20 \text{ m s}^{-1} \). For this case, \( V_o/(c_o \varepsilon_Y) \sim 4 \) (assuming \( \varepsilon_Y = 0.001 \)) and the overall strain rate is \( \dot{\varepsilon} = V_o/H = 1333/s \). In this range the material strain-rate dependence is understood to be important. The other dynamic effects that are also expected to play an important role include the inertial resistance of the core to the motion of the front face-sheet and the consequent plastic wave propagation in the truss core, and the inertial stabilization of the truss core that delays the onset of the truss buckling, thereby maintaining the effective strength of the core to much larger crushing strains than observed under quasi-static crushing.

Fig. 10 shows a sequence of selected frames taken from a typical dynamic compression test for an AA6061-T6 truss sub-element. The exposure time for each frame is 30 ns, while the inter-frame time is 5 \( \mu \text{s} \). The images in Fig. 10 represent every two frames taken from the photography sequence; thus the time interval between photos is 10 \( \mu \text{s} \). For typical elastic and plastic wave speeds, \( c_o = 5000 \text{ m/s} \) and \( c_p = 500 \text{ m/s} \), and the elastic and plastic wave fronts are expected to reach the back face at times \( t = 3 \) \( \mu \text{s} \) and \( 30 \) \( \mu \text{s} \), respectively. Fig. 11a shows the voltage vs. time signal showing the incident pulse, the reflected pulse and the transmitted pulse signals obtained in Test 1. Fig. 11b shows the corresponding force vs. time and the strain vs. time signals obtained from the measured incident and the transmitter bar strain profiles using Eqs. (2)–(4). Superimposed on the figure are selected frames from the high-speed video of the dynamic crush event. The amplitude of the force in the input stress pulse is 126 kN with a rise time of approximately 50 \( \mu \text{s} \). The maximum crush force in the truss sub-element is 2.2 kN, and is reached at \( \sim 70 \) \( \mu \text{s} \) after the stress pulse loading. Thereafter the force carried by the truss sub-
element is observed to decrease. This decrease in force is understood to be due to the initiation of elastic/plastic buckling in the ligaments as the stress builds up in the truss sub-element. As mentioned before, the time taken for the plastic wave to propagate to the back surface of the truss sub-element is $\frac{l}{C_24}$ after the arrival of the incident stress pulse. However, the first evidence of the initiation of plastic instability occurs in frame 4 (at 111 μs); the progression of damage/buckling can be observed after frame 6 (141 μs). This provides direct evidence of the beneficial effects of micro-inertia in delaying the initiation of plastic instability within the ligaments of the truss sub-elements. As the dynamic compression proceeds, the instability spreads to the other ligaments and eventually the entire core as the sub-element is fully crushed.

Fig. 12 shows the post-test pictures of the Al-6061 alloy truss sub-elements. In all the high-velocity impact experiments (impact velocity $\sim 20$ m s$^{-1}$), the truss sub-element structures are observed to completely crush during the dynamic deformation process. However, for the case of the 10 m s$^{-1}$ impact velocity, the truss sub-element is observed to undergo only plastic buckling, and is recovered prior to complete crushing of the truss core. At the room temperature, the peak compressive forces in AA6061-T6 sub-elements ranged from approximately 2.2 kN to 2.5 kN, while those in AA6061-OA sub-elements were
approximately 1.4 kN. At the lower-than-room test temperatures (−170 °C), the peak compressive forces in the AA6061-T6 sub-elements were in the range of 3.0–3.3 kN, while for the AA6061-OA sub-elements the peak forces were 2.1 kN. Also, for the room temperature tests, the specific energy absorption ranged from 16 to 21 J kg⁻¹ for the AA6061-T6 sub-elements, while for the AA6061-OA sub-elements the absorption was ~11 J kg⁻¹. At the lower-than-room test temperatures (−170 °C), the specific energy absorption in the AA6061-T6 sub-elements was approximately 28 J kg⁻¹, and that of AA6061-OA sub-elements was approximately 21 J kg⁻¹. Thus, the levels of crush resistance and impact energy absorption are much higher for the case of the T6 alloys (higher strength truss core materials), and at lower-than-room test temperatures. Table 4 summarizes the main results of the tests: it provides the average nominal strain-rate achieved in the tests, the maximum compressive force sustained by the truss core (maximum crush force), and the specific energy absorption obtained from all the dynamic compression tests conducted in the present study at both the room and lower-than-room test temperatures.

The dynamic force vs. displacement curves, for both the T6 and the OA heat-treatments (presented in Fig. 9), are higher by a factor of 2 when compared to those obtained under quasi-static deformation conditions (~6.7 × 10⁻² s⁻¹). The beneficial effects on energy absorption are understood to be due to the elevated strain rates and the low test temperatures that result in an elevation of the force vs. displacement curve during testing. This must occur due to changes in the deformation and flow behavior of these aluminum alloys when tested at low temperature and high strain rates, as summarized in Tang [19], and discussed below. Perhaps more importantly, this elevation of the force vs. displacement curve is a result of the micro-inertia in the truss ligaments that results in a delay in the buckling instability, as shown in Fig. 10, thereby increasing the crush resistance.

The beneficial effects of testing various aluminum alloys at low test temperatures and/or high strain rates have occasionally been reported for Charpy impact testing as well as fracture toughness testing of a variety of commercial aluminum alloys [8]. Tabular summaries of various aluminum alloys have also indicated measurable increases in the quasi-static yield strength, UTS and notch tensile strength on reducing the test temperature from room temperature to −320 °F, with further increases on going down to −452 °F. Recent results [18,19] clearly show that increasing the strain-rate as well as decreasing the test temperature increased the yield and UTS, without significantly degrading the reduction in area for 6061 in both the T6 and OA

<table>
<thead>
<tr>
<th>Test no.</th>
<th>AA6061 heat treatment</th>
<th>Impact velocity of striker bar (m s⁻¹ AA6061)</th>
<th>Test temperature (°C)</th>
<th>Average strain-rate (s⁻¹)</th>
<th>Max crush force (kN)</th>
<th>Specific energy absorption (N m kg⁻¹)</th>
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<td>Al-6061-T6</td>
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<td>24</td>
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conditions. Furthermore, in situ high-speed video that was integrated with the high-rate uniaxial test results have enabled a comparison of calculated necking instability strains to those revealed via the high-speed video under these different test conditions. The results clearly showed an elevation of the tensile necking strain with an increase in strain-rate and decrease in test temperature. This effect was maximized at the highest strain rates and lowest test temperatures. The source of the increased necking strain was shown to be due to the increased work-hardening rate exhibited by the samples tested under these conditions. The increased work-hardening rate counteracts the geometric softening experienced during tensile deformation, thereby prolonging the uniform strain regime. The increased yield stress, UTS and work-hardening rates obtained under these conditions indicate that the energy-absorbing ability of the material under tensile or compressive conditions should increase with respect to that obtained at room temperature. This has been documented in other published summaries [19], although the source(s) of these improvements were not discussed.

The present tests on the AA6061-T6 and AA6061-OA truss sub-elements, where the buckling instability is inhibited at low temperatures and high strain-rates thereby increasing the crush resistance, are broadly consistent with these other recent observations made on the effects of high strain-rate and low temperature on the energy absorbed during tensile or compression testing of monolithic AA6061-T6 and AA6061-OA. The increased yield strength, UTS and work-hardening rate exhibited by the monolithic AA6061-T6 and AA6061-OA obtained at high strain-rates and low test temperatures are consistent with the elevated load vs. displacement curves obtained during high-rate/low-temperature testing of the truss sub-elements (Fig. 9). The delay of the buckling instability in the truss sub-element ligaments that accompanies these rate and temperature induced changes in properties thereby increases the crush resistance and energy-absorbing characteristics of such structures constructed of these monolithic aluminum alloys. Additional work is needed on structures constructed from other aluminum alloys and heat treatments in order to determine if the presently reported results are broadly representative of the behavior of other aluminum alloy structures.

4. Conclusions

In the present study, the dynamic compressive behavior of AA6061-based open-cell tetrahedral core truss structures under two different heat treatment conditions, T6 and OA, has been investigated under uniaxial dynamic compression loading over a range of test temperatures from room temperature down to $-170 \, ^\circ C$. At all loading rates, truss structures constructed of AA6061-T6 exhibit a higher crush resistance and greater energy-absorbing capability than those constructed of AA6061-OA, which is in good consistency with the previous studies on the dynamic compressive properties of monolithic AA6061-T6 and AA6061-OA materials. In addition, lower test temperatures were found to produce significant effects on the force–displacement curves of the truss structures. Reduced test temperatures increase the crush resistance and mechanical energy absorption capability of the truss structures tested in the present study. These results have important implications in the design and performance of blast-resistant lightweight truss structures, and indicate that at lower than room temperatures and at elevated strain-rates, the truss structure made from these alloys have a potential to sustain higher deformations and absorb more impact energy prior to complete failure. The dynamic compressive deformation process of the truss structure was also captured by a high-speed camera.

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