High Rate Tensile Strength Measurements of Frangible Bullets Using a Kolsky Bar

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ABSTRACT

The tensile strength of frangible bullets is measured by a high rate diametral compression test (DCT) performed with a Kolsky Bar. Frangible bullets, meant to disintegrate on impact by brittle failure, also exhibit significant plasticity in compression. As a result, the elastic stress analysis usually used for the DCT cannot accurately deduce a tensile strength from the test results. To account for plasticity effects, finite element analysis is used to calculate the stress throughout the disk as it deforms under diametral loading prior to failure. Two cases are examined. The first involves a standard disk-shaped sample, and the second involves a disk with its contact points pre-flattened to reduce the amount of plastic strain prior to failure. With the standard sample, plastic flow produces tensile stresses at failure that are 80 % larger than those predicted by the usual elastic analysis of the DCT. Stresses at failure with the pre-flattened sample are 33 % lower than the elastic prediction, owing to load distribution and three dimensional effects. Results with the pre-flattened sample are considered more reliable since the finite element calculation does not account for crushing damage that is observed at the contacts points with the standard sample but is avoided with the pre-flattened sample.

INTRODUCTION

Frangible ammunition is designed to disintegrate on impact when the bullet strikes a hard surface. Frangibility is intended to improve the safety of target shooting ranges by preventing damage from errant shots or ricochets. However, there are concerns regarding the safety of these bullets when striking more compliant surfaces, particularly soft body armor. To address this safety issue, NIST is investigating the impact behavior of frangible bullets on soft body armor. Currently under investigation is a sintered copper-tin composite frangible bullet (90 % copper by mass). Frangibility in this material arises from the presence of several brittle intermetallic phases that form during the sintering process, as well as approximately 5 % porosity.

The initial phase of this study focused on the dynamic failure behavior of the frangible bullets. Since failure on impact is likely the result of tensile stresses, it was of interest to measure the tensile strength of the frangible bullets at high loading rates. A convenient method for measuring the tensile strength of brittle materials dynamically is the diametral compression test [1]. In this test, a thin disk is loaded along its diameter, creating a uniform tensile stress that acts perpendicular to the applied load. As the load is increased, the tensile stress rises until a crack forms, breaking the sample in two. Because of its simplicity, this test is readily adaptable to a compression Kolsky Bar [2] which can provide the necessary dynamic loading.

Assuming the disk deforms elastically prior to failure and that plane-stress conditions are realized, the tensile stress across the middle of the disk is simply related to the applied load \( P \) through [3]:

\[
\sigma = \frac{2P}{\pi Dt}
\]  

(1)
In Eqn. 1 $D$ is the diameter of the disk sample and $t$ its thickness. This analysis also assumes idealized line loading of the disk. However, two difficulties arise when applying the diametral test to the frangible bullets studied here. First, the sample geometry chosen for the study is significantly thicker ($t/D = 0.5$) than recommended for the diametral test ($t/D < 0.25$). Thick samples were easier to align and more robust for use in the Kolsky Bar. Further, using a sample with nearly the same dimensions as the full size bullet increases confidence that the failure strength measurements derived from the test are applicable to whole bullet impact studies since the tensile strength of brittle materials often varies with specimen size [1]. The drawback to using thick samples, however, is that the sample is no longer in a state of plane stress, invalidating one of the assumptions underpinning Eqn. 1. The second difficulty is that this frangible material, although it behaves as a brittle material in tension, exhibits significant ductility in compression. Ductility comes from the large pure copper matrix phase surrounding the brittle intermetallic phases and pores. Ductility will cause the ends of the sample to flatten during the diametral test, distributing the load over a larger area and invalidating the assumption of ideal line loading that also underpins Eqn 1. Flattening at the contact points also reduces the portion of the diametral plane exposed to uniform stresses, causing further deviations from the ideal analysis [4]. Finally, plastic deformation at the contacts likely alters how stresses develop in the inner elastic portion of the sample compared to purely elastic deformation, casting further doubt on usefulness of the traditional analysis method for this application.

To address these problems, rather than relying on Eqn. 1 to assess the failure strength in this diametral test, finite element modeling is used to compute the stress distribution in the disk specimen as it is loaded dynamically in the Kolsky Bar test. The finite element results are then compared to the ideal elastic analysis at the experimentally-determined failure load. In addition, the effect of pre-flattening the disk to inhibit plastic deformation and possible crushing damage is investigated experimentally and with finite element modeling.

**EXPERIMENT: DIAMETRAL COMPRESSION TEST USING A KOLSKY BAR**

The Kolsky Bar (or Split-Hokinson Bar) technique is a well-established method for testing materials at high strain rates. It is thoroughly described elsewhere [2]. In this technique, a test sample is sandwiched between two long thin metal bars. The first (incident) bar is struck with a special projectile, which creates an elastic compressive pulse that travels rapidly down the incident bar toward the sample. Upon reaching the sample, the pulse generates a large compressive load, causing the sample to rapidly deform. The NIST’s Kolsky Bar uses 1.5 m long by 1.5 cm diameter maraging steel compression bars. Load pulses for the diametral tests are generated by propelling a 375 mm long striker bar (also made from maraging steel) at velocities up to 25 m/s into the first compression bar using an Argon gas gun.

In the standard Kolsky Bar test, the geometry of the sample is chosen such that the stress-strain behavior of the sample and the strain rate during deformation can be determined from the elastic pulses transmitted through and reflected from the sample as it deforms. For the diametral compression tests, only the load transmitted through the sample during deformation is needed to compute the failure strength. This transmitted load is determined from the strain signal in the second compression bar, $\varepsilon_t$, using:

$$P = A E \varepsilon_t$$

(2)

Here $A$ is the compression bar cross-sectional area, and $E$ is the elastic modulus of the compression bar (200 GPa). The strain pulses are measured using a single 1000 ohm metal foil strain gage placed at the midpoint of each compression bar. The response of each strain gage is determined using a single-arm Wheatstone bridge arrangement powered by a 24 V DC supply. The strain signals are sampled at 20 MHz using a digital storage oscilloscope.

For the diametral compression test, a gradually ramped compression pulse (rising saw-tooth shape) is needed so that the load applied to the sample increases steadily until failure. This is different from the standard Kolsky Bar test which requires a square input pulse. A ramped compression pulse is created by placing a soft copper pulse-shaper (3 mm diameter by 4 mm long cylinder) on the face of the first compression bar to soften the impact of the striker bar. Example compression pulses are shown in the next section. Finally, the tensile strength of the frangible material is determined by first noting the strain in the transmitted pulse signal at failure and evaluating Eqn. 2 for the failure load $P$. The failure load is then used to evaluate Eqn. 1 for the failure stress. This traditional analysis is then compared to the tensile stress computed using finite element analysis.
FINITE ELEMENT MODEL

ABAQUS/Explicit\(^1\) [5] finite element software is used to calculate the stress field in the diametral sample as it deforms under the compressive pulse loading applied by the Kolsky Bar. The diametral sample, consisting of a 4.5 mm-thick, 9 mm diameter squat cylinder, is placed between the compression bars as shown in Fig. 1. Only one quarter of the test geometry is modeled, with symmetry imposed on surfaces facing the \(<1>\) and \(<-2>\) directions. Both compression bars and the sample are modeled with C3D8R hexahedral elements. Approximately 27,000 elements are used to model the quarter sample while 225 elements are used for each compression bar. This mesh density provided relatively smooth contact behavior between the sample and the compression bars and good resolution of the stress field in the sample. To reduce computation time, neither the striker bar nor the pulse shaper is modeled. Instead, the incident stress wave is created artificially by specifying a pressure boundary condition on the incident compression bar that closely matched the experimental stress pulse. Time stepping is controlled automatically by the ABAQUS software using the element-by-element option. Contact between the compression bars and the sample is modeled using the kinematic hard contact formulation. Friction is neglected. The maraging steel compression bars are modeled as linear elastic solids with the following properties: \(E = 2.0 \times 10^{11}\) Pa, \(v = 0.29\), and \(\rho = 8100\) kg/m\(^3\). The frangible bullet material model is discussed in the next section.

![Figure 1](image)

**Fig. 1** Quarter of diametral sample and Kolsky bars modeled using finite element analysis. Bar and sample planes facing \(<-1>\) and \(<-2>\) directions are symmetry planes. The elastic compression wave travels in the \(<-3>\) direction.

FRANGIBLE MATERIAL MODEL

The frangible Cu-Sn composite consists of a pressed and sintered mixture of 90 % pure copper powder and 10 % pure tin powder (mass basis). A representative microstructure is shown in Fig. 2. It consists of copper powder particles surrounding two intermetallic phases (Cu\(_6\)Sn\(_5\) and Cu\(_3\)Sn) that bound a small amount of un-reacted tin. The structure also contains approximately 5 % porosity by volume. Accounting for the alloy composition and the

\(^1\) Commercial products are identified in this work to adequately specify certain procedures. In no case does such identification imply recommendation or endorsement by NIST, nor does it imply that the materials or equipment identified are necessarily the best available for the purpose.
porosity leads to a density of 8040 kg/m³. Quasi-static compression tests yielded an elastic modulus of $E = 79$ GPa, and Poisson’s ratio is assumed to be $\nu = 0.3$. The inelastic response of the material was determined from both quasi-static and high strain rate (1500 1/s) compression tests. Plastic strains of up to 0.12 were observed in compression at both low and high strain rates. Although some cracking was observed in the compression specimens, the samples retained strong overall cohesion despite the large strains. Possible effects of cracking in the compression zone on the stress calculations are discussed later. Very little strain hardening was observed in the compression tests, and strain rate effects were also minor. As a result, the frangible sample was modeled as an isotropic, elastic, perfectly-plastic metal with a yield strength of 294 MPa. No attempt was made to account for porosity.

RESULTS

Fig. 3 compares experimental strain pulses with strain pulses computed from the finite element model. The close agreement between model and experiment on the transmitted bar strain signals indicates that the gross deformation of the sample is correctly captured by the model. The minor ripples apparent in the simulated transmitted strain signal are artifacts of the discretization of the contacting surfaces in the model. The ramped input pulse applies a linearly increasing load on the diametral sample, which is apparent from the transmitted strain signal. At fracture, the transmitted strain signal drops suddenly as the sample can no longer transmit the load to the second compression bar. The peak strain just before fracture is used to compute the failure load. Because the finite element model does not include a fracture criterion, the simulated transmitted strain continues to rise past the experimental fracture point. The stress distribution at failure is therefore obtained from the model at the experimentally-determined failure load.

The tensile stress distributions on the diametral plane (acting perpendicular to the load direction) are plotted in Fig. 4 for several sample load levels. The failure load determined from repeated experiments is 4100 N ± 250 N (2k expanded uncertainty, 4 tests). At well below this failure load ($P = 1000$ N), the tensile stress distribution is very uniform over a large portion of the disk. At this load the deformation is primarily elastic, and the stress distribution is therefore close to the ideal case. However, some three-dimensional effects are evident at this load: the compressive stress at the ends of the sample drops off towards the free surface of the sample. As the load increases, the tensile stress distribution becomes increasingly non-uniform. Both the flattening of the contact interface and the plastic flow induced by the high compressive stresses at the contact points play a role in the evolving stress distribution. As the compressed zone increases in size, the portion of the disk exposed to tensile stresses becomes smaller. At failure, only 60 % of the diametral plane is under tension.

To compare the tensile stress at failure predicted by the finite element model with that predicted by Eqn. 1, the model results are averaged over the portion of the sample under tensile stress in the <2> direction. Table 1
Fig. 3 Comparison of experimental incident and transmitted strain pulses with ABAQUS simulation for a typical diametral compression test.

Fig. 4 Evolution of tensile stress (in Pascals) in the <2> direction on the diametral plane with applied load. Top: P = 1000 N; middle: P = 2700 N; bottom: P = 4100 N (failure load).

compares the average stress over the diametral plane against Eqn. 1 at different applied loads. At 1000 N, the model solution is within 5% of the elastic, plane-stress analysis. At higher loads, however, the finite element model predicts increasingly larger tensile stresses. Near the experimental failure load (4100 N) the finite element model predicts stresses almost twice as large as Eqn 1.
Table 1  Evolution of average tensile stress with applied load predicted by elastic-plastic finite element model and corresponding elastic-only stress prediction for the disk specimen.

<table>
<thead>
<tr>
<th>Load [N]</th>
<th>Elastic Plane-Stress Model</th>
<th>Elastic-Plastic Finite Element Model</th>
</tr>
</thead>
<tbody>
<tr>
<td>1031</td>
<td>16.2</td>
<td>17</td>
</tr>
<tr>
<td>2738</td>
<td>43.0</td>
<td>68</td>
</tr>
<tr>
<td>4078a</td>
<td>64.1</td>
<td>118</td>
</tr>
</tbody>
</table>

*a* experimental failure load = 4100 N

The amplification of the tensile stresses observed in the finite element results is due primarily to the dilation of the ends of the sample by plastic flow at the contact points. At failure, the model indicates peak equivalent plastic strains of approximately 8 % in the compression zones. At this level of yielding, the ends of the specimen are severely flattened, and material in the compressed zone flows plastically perpendicular to the load direction. This dilation of the ends of the sample acts to reinforce the tensile stresses of interest acting on the interior diametral plane, resulting in higher stresses than would have existed had no yielding occurred. By itself, flattening of diametral samples has been shown to actually reduce the tensile stresses on the diametral plane due to more distributed loading [3]. In the present study, the plastic flow at the contact points appears to override this effect, resulting in higher tensile stresses on the interior diametral plane.

While this result is physically reasonable, it must be recalled that the material model used in the foregoing calculation ignores any effects relating to brittle damage that might develop in the compressed zones. Some cracking was usually observed in these zones on broken samples, as shown in Fig. 5. It is not certain whether this damage occurs before or after the primary failure of the sample. High speed movies of the sample fracture indicate that this crushing damage does not initiate failure, which would invalidate the test. However, any cracking that occurs in the compression zones prior to failure will affect the stiffness of the material in these regions, which in turn will alter how stresses are transmitted to the interior of the sample where failure occurs. By ignoring this in the finite element model, the computed stress distribution may not reflect the actual stress condition of the disk just prior to failure.

Several additional mechanical tests would be needed to model the effect of local crushing damage on the stress distribution in the sample. Alternatively, the large compressive strains that cause this damage can be avoided by

Fig. 5 Brittle crushing damage observed in compression zones. Left: macro-scale damage on broken diametral sample (original diameter is 9 mm). Right: micro-scale damage near surviving contact surface (length scale bar is 100 µm).
pre-flattening the disk at the contact points. This pre-flattening technique has been used successfully with brittle specimens to reduce contact stresses and thereby prevent premature shear or crushing failures, which would invalidate the test [3]. Here it is used to reduce or eliminate the plastic strains in the contact region to minimize the possibility of crushing damage and thereby increase confidence in the finite-element results while relying on the simplified perfectly-plastic material model for the frangible bullet.

A second set of experiments was conducted with a pre-flattened sample, where the flats equaled 25 % of the specimen diameter, or 2.25 mm. Fig. 6 compares the results of FEA simulations with these experiments. Again, the overall agreement between measured and simulated stress pulses is good. Fig. 7 compares the equivalent plastic strains on the diametral plane with and without flats. As Fig. 7 shows, in the pre-flattened sample the plastic strains are negligible in comparison to those in the standard sample prior to failure. Fig. 8 compares the tensile stress distribution on the diametral plane for the pre-flattened sample at the experimental failure load of 4100 N (± 200N, 2k expanded uncertainty, 6 tests) with that of the standard sample at its failure load of 4100 N. The finite element model indicates the average tensile stress at failure for the pre-flattened sample is 43 MPa, less than half the value computed for the standard sample. The results also indicate that the tensile stresses are more uniform and act over a larger portion of the diametral plane for the pre-flattened case. The standard deviation of the tensile stress at failure is 14 MPa with the pre-flattened sample compared to 50 MPa for the standard sample. The pre-flattened sample experiences tensile stresses over 69 % of the diametral plane compared 63 % for the standard sample.

Ideally, the failure strength of the material as indicated by the DCT should be the same whether the sample has been pre-flattened or not. However, the model results indicate two very different answers for the failure stress. As already discussed, the stress calculation for the un-flattened sample is likely in error since the effects of brittle damage at the contact points in the experiment is not included in the model. The presence of cracks in the compression zones of the sample most likely indicates that tensile stress acting on the diametral plane in the experiment are smaller than the model calculations since the calculations assume that the dilated regions remain stiff. Thus the model of the un-flattened sample probably overestimates the stress acting on the diametral plane. To confirm this however, new finite element calculations must be carried out that incorporate a brittle damage model for the frangible bullet. Alternatively, since there is very little plastic yielding in the pre-flattened sample prior to failure, it is likely that the predicted stress distribution is sufficiently accurate to be used, in conjunction with the experimental results, to obtain the most reliable possible failure strength data from this DCT.

![Graph](image.png)

**Fig. 6** Comparison of experimental incident and transmitted strain pulses with finite element simulation for a diametral compression test with a pre-flattened sample.
**Fig. 7** Comparison of equivalent plastic strains on diametral plane for diametral sample without flats (top) and pre-flattened sample (bottom) at the experimental failure load (4100 N).

**Fig. 8** Comparison of tensile stress distribution (in Pascals) in the <2> direction on diametral plane at failure of sample without flats (top) and sample with flats (bottom) at the experimental failure load (4100 N).

Finally, compared to the elastic plane-stress analysis (Eqn. 1), which predicts a failure stress of 64 MPa at a load of 4100 N, the model prediction of the failure stress with the flattened sample is about 33% lower. This result is due to the greater distribution of the load over the sample surface, the drop in tensile stress near the compression zones, and three-dimensional effect of stress variations near the free surface of the sample.

**CONCLUSIONS**

Diametral compression tests (DCTs) were performed in a Kolsky Bar to determine the dynamic tensile strength of a partially-sintered Cu-Sn composite material that, although brittle in tension, exhibits significant plastic flow in compression. A three-dimensional finite element model was used to estimate the tensile stress in the sample as it deforms dynamically using an elastic perfectly-plastic material model for the sample. The model-predicted tensile strength was 80% higher than that indicated using the traditional plane-stress elastic analysis of the DCT due to plastic flow at the contact points. However, since the model ignores effect of brittle crushing damage that occurs at the contact points, this result may be in error. To avoid this brittle damage and excessive plastic flow, tests
were conducted with samples with pre-flattened contact points. The tensile stress at failure for the pre-flattened sample was less than half that computed for the standard sample and was 33 % lower than the traditional elastic, plane-stress analysis assuming no flats. The latter result is due to the suppression of plastic yielding coupled with more distributed loading, the drop in tensile stresses near the compression zones, and free-surface effects. Failure strengths computed for the pre-flattened samples are considered more reliable since the deformation prior to failure is primarily elastic and therefore easier to model accurately.

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