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UNDERWATER IMPLOSION OF CYLINDRICAL METALLIC SHELLS IN CONFINING ENVIRONMENTS

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UNDERWATER IMPLOSION OF CYLINDRICAL
METALLIC SHELLS IN CONFINING ENVIRONMENTS

BY

SACHIN GUPTA

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DOCTOR OF PHILOSOPHY DISSERTATION
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ABSTRACT

A fundamental experimental investigation was conducted to understand the underwater dynamic implosion of cylindrical metallic shells. In particular, the attention was focused on studying the generation of pressure waves from implosion in varying confining situations such that the fluid structure interaction with the confined structures governs the mechanics of implosion process. The experiments were conducted in three environments: (a) free-field environment represented by a large underwater pressure vessel facility, (b) partially closed environment represented by one-end open confining tube, (c) completely closed environment represented by closed confining tube. This study also utilizes the visualization and understanding of the real-time deformation of the implodable volumes using high speed 3-D Digital Image Correlation (DIC) technique. This technique was modified and recalibrated for each environment to accurately capture the full-field underwater deformations and velocities of the collapsing implodable volume. Dynamic tourmaline pressure transducers along with face pressure transducers were used in the experiments to capture the near field pressure history during the implosion event. Both structural DIC measurements and pressure signatures were synchronized for understanding the fluid structure interaction process.

The free-field experiments were conducted with varying length to diameter ratio implodable volumes to study the basic implosion mechanics in the absence of confining structures. Two separate implosion studies were conducted in one-end open confining tube. First study was aimed to understand the general physics of water hammer wave evolution in the open-ended confining tube in conjunction with DIC

measurements. Second study addresses the problem of sympathetic implosion of adjacently placed structures in the confining tube. Four studies were conducted in completely closed confined environment. First study evaluates the mechanics of implosion occurring inside a closed confining tube with centrally placed implodable volumes. It also looks to understand the effect of increasing size of the implodable volume on the confined implosion mechanics. Second study was conducted to examine the changes in implosion mechanics due to longitudinal offset location of the implodable volume. Third study extends the first study by performing real time high speed DIC measurements to correlate the evolution of implosion waves with implodable volume's deformations. This also investigates the effect of trapped air bubble inside the closed confining on the rate/extent of collapse in implodable volume. Fourth study was conducted to understand the shock initiated implosion mechanics inside the confining tube with DIC measurements.

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PREFACE

Experimental investigations have been conducted to study the underwater dynamic implosion of cylindrical metallic shells. The dissertation is an attempt to investigate the fluid structure interaction and its relation to the generation of pressure waves in an underwater implosion. This dissertation is prepared in the “*manuscript format*”.

Chapter 1 provides an overview of the current and previous literature in the area of underwater implosion. This chapter serves as brief background in this area as well as a general introduction for the following chapters in this dissertation.

Chapter 2 focuses on the free-field implosion experiments with varying length to diameter ratio aluminum implodable volumes. The objective of this study was to investigate the physical processes associated with the implosion of cylindrical tubes in a hydrostatic free-field underwater environment using high-speed three-dimensional digital image correlation (3D DIC) technique. This chapter has been published in the *Proceedings of the Royal Society A: Mathematical, Physical and Engineering Science*.

Chapter 3 looks to understand the mechanics of implosion occurring inside a one end open confining tube. The experiments with varying collapse pressure implodable volumes were conducted with full-field measurement of implodable volumes’ deformations using 3D DIC technique. This chapter explains the evolution of detrimental water hammer waves observed in these experiments. This chapter has been prepared with the guidelines specified by the journal of *Experimental Mechanics*.

Chapter 4 discusses the problem of sympathetic implosion for adjacently placed structures in the one end open confining tube. The experiments were conducted

with two different collapse pressure implodable volumes, placed together inside the one end open confining tube. This chapter explains the physical process responsible for sympathetic implosion of adjacent structures. This chapter has been published in the journal of *Extreme Mechanics Letters*.

Chapter 5 investigates the mechanics of implosion occurring inside a closed confining tube with centrally placed implodable volumes. The volume ratio between the implodable volume and the confining tube was increased, while the collapse pressure was held approximately constant across experiments. This chapter has been published in the *International Journal of Solids and Structures*.

Chapter 6 extends the study of chapter-5 by investigating the implosion of longitudinally off-centered placed implodable volumes within the confining tube. Two longitudinal offset were chosen for a single geometry in order to explicitly capture the effect of location. This chapter has been published in the *Journal of Applied Mechanics*.

Chapter 7 is a comprehensive investigation, extending chapter-5, by incorporating DIC technique for confining tube implosion experiments. The changes in structural deformation/velocity due to confining tube are discussed in this chapter. This chapter also evaluates the effect of a trapped air bubble inside the confining tube on the mechanics of implosion. This chapter has been prepared with the guidelines specified by the journal of *Experimental Mechanics*.

Chapter 8 details the experimental study on the shock initiated implosion of cylindrical implodable volumes inside a confining tube. These experiments seek to understand the effect of pre-hydrostatic pressure on the initiation of implosion process.

The DIC technique is used to capture the real-time vibrations/collapse of the implodable volume during this study. This chapter has been prepared with the guidelines specified by the *Journal of the Mechanics and Physics of Solids*.

Chapter 9 summarizes the major findings of the topics discussed in this dissertation.

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CHAPTER 1

INTRODUCTION AND LITERATURE REVIEW

Understanding the fundamental mechanisms associated with the implosion process has been a topic of interest since the early 1950's, especially in the marine pipelines and naval communities (Isaacs and Maxwell, 1952; Palmer and Martin, 1975; Turner, 2007; Urick, 1963). Typical examples of implodable volumes include deep ocean submersibles, submarines, underwater remote operated vehicles, underwater pipelines, and underwater sensors (Turner and Ambrico, 2012). An implodable volume can be defined as any structural shell or body that is acted upon by external pressure and contains internal gas at a lower pressure (or vacuum). In simple terms, the implosion of a structure can be understood as a sudden loss of stability due to a net external force causing the structure to collapse onto itself. The resulting collapse of the structure is violent and results in rapid release of energy in the form of shock pressure waves, high velocity fluid motion, and sound (LeBlanc et al., 2014). An axial/lateral loading, uniform hydrostatic pressure or a combination of both can be the initiating and driving forces for the implosion of a structure. In underwater environments, this collapse is a dynamic process with duration of the order of milliseconds. At the onset of collapse, the implodable volume walls gain inwards momentum and the surrounding water rushes in to fill the resulting void generated in the collapse process. The fast inward travelling water surrounding the receding walls of a collapsing structure stops suddenly when the walls come into contact. However,

the acquired momentum of the in-rushing water causes it to over-compress against the structure and produces strong outwardly radiating shockwaves. Such pressure pulses/shock waves can be large enough to potentially damage or even lead to the implosion of adjacent structures (Diwan et al., 2012; Farhat et al., 2013; Harben and Boro, 2001; Ikeda, 2012; Ling et al., 2013; Orr and Schoenberg, 1976; Turner and Ambrico, 2012; Vath and Colletti, 1968). A classic example of such an event is the 2001 accident at the Super-Kamiokande facility in Japan, where about 7000 photomultiplier tubes were destroyed by a sympathetic implosion event (Cartlidge, 2001).

In the past, many researchers have theoretically investigated the buckling of cylindrical shells to predict the critical hydrostatic collapse pressures (Timoshenko and Gere, 1963; von Mises, 1929). The effect of imperfections and various defects has been presented in several research articles (Budiansky and Hutchinson, 1966; Simitses, 1986). It was concluded that the initial ovality of the cylindrical shell can significantly reduce the collapse pressure, while the variation in wall thickness has a minimal effect on the collapse pressure (Kyriakides and Corona, 2007). The propagation of buckles in offshore pipelines has also been widely studied (Charter et al., 1983; Kyriakides and Babcock, 1981; Kyriakides and Netto, 2000; Mesloh et al., 1973; Palmer and Martin, 1975).

Although the problem of buckling has been extensively investigated from the structural point of view, there have been very few studies reported which aim to understand the fluid motion and pressure wave emissions during underwater buckling of structures. In the early 1900's, Rayleigh (Rayleigh, 1917) developed analytical

expression for the collapse of a spherical bubble inside an incompressible fluid. According to this theory, a pressure difference between the bubble and ambient internal pressure causes the bubble to collapse or grow, which results in an oscillatory bubble pulse. In the problem of a closed structure imploding in an underwater environment, the low pressure gas is contained inside a structure. Due to this fact, this structure plays a key role during the collapse process and the complex fluid-structure interaction between the structure and water governs the dynamics of the implosion. In the early 1960's, the implosion of glass spheres was used to generate acoustic signals for underwater applications (Isaacs and Maxwell, 1952; Urick, 1963). Orr and Schoenberg conducted implosion experiments using pre-weakened glass spheres (by grinding a flat spot) in the ocean and concluded that the implosion depth is dependent on the thickness of the flat spot (Orr and Schoenberg, 1976). Harben and Boro conducted implosion experiments with five glass spheres bundled together for boosting the amount of implosion energy released (Harben and Boro, 2001). Turner conducted near-field pressure measurements during the implosion of glass spheres and concluded that the failure time history of the structure has a significant influence on an implosion pressure pulse. Recently, Turner and Ambrico (Turner and Ambrico, 2012), and Farhat et al. (Farhat et al., 2013) studied the implosion of aluminum cylindrical tubes. Turner and Ambrico concluded that there are four primary features of the implosion process in metal tubes: (1) the initial collapse phase, prior to wall contact, is accompanied by a smooth decrease in pressure in the surrounding water, (2) at the moment that contact is made between opposing sides of the collapsing cylinder at the center, a short duration pressure spike is emitted in the surrounding water, (3) a large

positive pressure is produced at the instant that contact between the two opposing sides extends the full width of the cylinder, and (4) as the buckle propagates toward the ends, the pressure pulse continues, but at a lower magnitude, until the buckle reaches the end cap and the collapse of the cylinder completes. Farhat et al. (Farhat et al., 2013) extended this implosion work by studying both mode-2 and mode-4 collapse of aluminum cylindrical shells and demonstrated that the pressure pulse generated is influenced by the mode of buckling as well as the associated localization of collapse.

All these experimental and numerical studies on implosion mentioned above were conducted in a free-field environment, where the net hydrostatic pressure in the surrounding fluid is maintained during the implosion process and the free-field environment acts as an infinite source of hydrostatic potential energy to drive the implosion process. On contrary, in the case of implosion occurring within a confining space, the source of hydrostatic potential energy to progress the implosion is limited. Hence, the ratio of energy required to deform the implodable volume and the initial potential energy available acts as a critical factor to drive the implosion process. Costa and Turner (Costa and Turner, 2008) did study the implosion of a tube occurring within an open-ended confining tube and showed that the implosion phenomenon significantly differs from a free-field implosion, however, due to open ended nature of their confining tube, infinite energy was still available for driving the implosion process. Such implosions have shown to be capable of generating strong hammer pressure waves at the closed end of the confining tube, which is highly detrimental to the nearby structures. Therefore, implosion of the weakest implodable volume inside the confining tube may damage the adjacent stronger structures. As the evolution of

such implosion waves is a highly fluid structure interaction process, the structure coupled with the surrounding fluid leads to the generation of water hammer waves. Any changes in the design of the structure will cause to alter the fluid structure interaction process and so the strength of hammer waves. Therefore, in a real design, the mechanics of collapse can be completely different depending upon the geometry/location of the implodable volume in the confining tube. Thus, there is a need to understand the evolution of these waves from both structural deformation and the fluid mechanics point of view in order to predict the peak strength/impulse of these detrimental water hammer waves in real situations. To the best of author's knowledge, there have been no studies reported on relating the generation of water hammer waves with the structural deformations of the implodable volume.

Another real situation problem in a confining tube implosion process is the trapping of air bubbles in fluid filled pressure vessels. It enables for high compressible energy storage upon pressurization, and it can significantly alter the implosion mechanics within confining environments. No literature exists on understanding this aspect of trapped air bubble.

The experimental findings which address the problem of structural implosion occurring due to a shock wave/UNDEX loading are very limited. Although a similar problem has been theoretically investigated since early 1960's in terms of "dynamic buckling" of cylindrical shell structures (Goodier and McIvor, 1964; Lindberg, 1974; Lindberg and Florence, 1987), there are very few comprehensive experimental studies reporting "underwater" dynamic buckling of structures. A recent study on underwater buckling of tubular structures in a confining space was reported by Bitter and

Shepherd (Bitter and Shepherd, 2014). They studied the dynamic buckling of submerged tubular structures in a rigid closed outer tube. Dynamic buckling experiments were performed by applying impulsive loading in the water filled outer tube using a projectile and the fluid structure interaction models were developed to predict the dynamic response of the tube. In their experiments, the amplitude of impulse loading was an order of magnitude higher than the critical static buckling threshold, therefore the impulsive loading was only considered to cause buckling in the specimen and the effect of initial hydrostatic pressure was not specifically investigated. To the best of author's knowledge, there have been no studies reported on investigating the effect of hydrostatic pressure on shock initiated buckling/implosion of structures in confining environments. In real submarine situations, the hydrostatic pressure always acts on the structure (or the structures are always pre-hydrostatically loaded with certain value); hence an additional shockwave loading, which might not cause any damage to a structure at no hydrostatic pressure, may cause it to fail due to the presence of initial hydrostatic pressure. Therefore, the dynamic buckling design criteria must take into account for the typical operating hydrostatic pressure for safes design of the underwater structures.

With advances in computational computing resources and efficiency, the modeling and simulation of implosion phenomenon has gained interest. Early numerical studies involving underwater implosions generally represented the implodable volume as a gas bubble within a high pressure fluid field. Kadioglu and Sussman (Kadioglu and Sussman, 2008) utilized an adaptive solution methodology to solve the multi-phase problem of a gas bubble contained within a water field. Farhat et

al. (Farhat et al., 2008) simulated the 2 phase flow problem with a solution methodology known as the “ghost fluid method for the poor” (GFMP). The work extended the original GFMP method to better handle the large discontinuities of pressure and density at the air/water interfaces. These studies have generally focused on the two-phase nature of the fluid flow but have neglected the fluid structure interaction which has been shown to be important in the implosion of a structural body. Turner (Turner, 2007) utilized the coupled Eulerian-Lagrangian fluid structure interaction code (Dynamic System Mechanics Analysis Simulation code or DYSMAS) to simulate the collapse of glass spheres to determine the influence of the failure rate of the spheres on the resulting pressure histories. The primary conclusion was that a computational model of an underwater implosion event must include the structure that separates the low pressure air from the high pressure water. If the structure is neglected, the model would over predict the peak pressure generated from the collapse. Turner and Ambrico (Turner and Ambrico, 2012) also utilized the DYSMAS code to simulate the implosion of metallic cylindrical bodies. Farhat et al. (Farhat et al., 2013) investigated the pressure pulses resulting from an implosion, and the parameters which influence the nature of the pulses, through the AERO software suite. The simulations were shown to accurately capture both the deformations of the structure as well as the pressure waves resulting from the collapse. Recently, Chamberlin et al. (Chamberlin et al., n.d.) developed energy metrics to characterize underwater implosion and used these metrics to examine the energy balance during an implosion event. For a series of test cases involving hydrostatic implosion of ductile metal cylinders, they found that the structure absorbs the majority of the initial energy

of the system and the released pressure pulse carries away a small percentage of the initial energy (Chamberlin et al., n.d.).

In order to incorporate the high-speed 3D digital image correlation (DIC) measurements on the implodable volume, the effect of refraction due to the water body and any transparent window that is in the light path has to be accounted for (Haile and Ifju, 2012; Ke et al., 2008). Ke et al. (Ke et al., 2008) have described a rigorous method of accounting these effects in their work when the normal to window through which the object is viewed is inclined to the camera axes. Although this procedure has been shown to accurately measure both the three dimensional position and the displacements/strains of the submerged objects, the procedure of calibration and correction is complex, and it requires extensive post-processing of the data.

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CHAPTER 2

STUDY OF DYNAMIC UNDERWATER IMPLOSION MECHANICS USING DIGITAL IMAGE CORRELATION

by

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Abstract

The physical processes associated with the implosion of cylindrical tubes in a hydrostatic underwater environment were investigated using 3D high speed digital image correlation (DIC). This study emphasizes visualization and understanding of the real time deformation of the implodable and the associated fluid structure interaction phenomena. Aluminium 6061-T6 cylindrical tubing were used as the implodable volumes. Dynamic tourmaline pressure transducers were placed at selected locations to capture the pressure history generated during each implosion event. A series of small-scale calibration experiments were first performed to establish the applicability of 3D DIC for measuring the deformation of submerged objects. The results of these experiments indicated that the effects of refraction due to water and the optical windows can be accounted for by evaluation of camera's intrinsic and extrinsic parameters using a submerged calibration grid when the surface normal of the optical windows is collinear with the camera optical axis. Each pressure history was synchronized with its respective high speed DIC measurements. DIC results showed that the highest rate of increase in contact area correlates to the largest pressure spike during the implosion process. The results also indicated that for a given diameter, longer implodable volumes generated higher pressure spike.

Keywords

Digital Image Correlation, Submerged Objects, Implosion, Implodable Volume, Fluid Structure Interaction, High Speed Stereo-photography

1. Introduction

Submerged enclosed shells can collapse instantaneously under hydrostatic water pressure leading to release of a strong propagating pressure wave [1,2]. This process known as “implosion” has been of interest in the area of underwater acoustics since such implosions can be used as an alternative to underwater explosions (UNDEX) commonly used as an underwater sound source [1,2]. However, there are situations in which an implosion is undesirable as the pressure wave generated during the implosion can damage adjacent structures or initiate implosion of adjacent enclosed shells [3–5]. The implosion of several photo multiplier (PMT) tubes initiated by accidental implosion of a single PMT in the Super-Kamiokande neutrino laboratory in Japan in 2001 has sparked a renewed interest in studying the pressure waves generated by an underwater implosion [6].

Urick [1] conducted implosion experiments by submerging glass bottles having different size in the ocean and recorded the pressure profiles generated by their implosion using a hydrophone located close to the water surface. Urick concluded that the efficiency of energy conversion from potential energy of implodable volume to acoustic waves is $\sim 0.2\%$ to 0.6% . Orr and Schoenberg [2] performed both laboratory and field tests by imploding pre-weakened glass spheres and reported that the pressure pulses had a characteristic negative pressure head followed by a sharp positive spike region. Turner [7] investigated the implosion of glass spheres by performing laboratory experiments followed by numerical simulations of the experiments. His study indicated that the failure time history of the structure separating the low pressure air from the high pressure water has a significant effect on the character of the

pressure pulse. Numerical simulations indicated that if the behaviour of the structure is ignored, the model would over-predict the peak pressure generated in the implosion. The failure in these investigations [1,2,7] was brittle in nature, with the glass sphere fragmenting into several pieces. On the contrary, the collapse of metallic tubes is not catastrophic even though very rapid in nature. Recently, Turner and Ambrico [3] investigated the implosion of metallic aluminium tubes. Pressure pulses having characteristics similar to that obtained with glass spheres were observed in their experiments. The effect of collapse mode on the pressure pulse was investigated recently by Farhat et al. [8]. Their study indicated that higher mode of collapse generated a higher pressure peak but of smaller duration compared to that obtained in a lower mode collapse.

In the investigations reported above, quantitative measurements on the imploding shell were not reported. Ikeda et al. [9] reported that during an implosion process, the structure becomes so weak that the influence of the structural details on the pressure waves can be neglected and the structure behaves similar to a bubble of low-pressure gas. Hence, the pressure waves records and the time primarily scales with the critical collapse pressure and the time period of the equivalent size bubble oscillations respectively. In the context of Turner's observation [7], that the character of the pressure pulse is significantly influenced by the failure time history, it is important to experimentally capture the deformation history of the implodable and relate the same to the pressure pulse generated in order to suitably modify the imploding structure to mitigate the implosion induced pressure pulse. With this intention, implosion experiments were performed using 38.1 mm (1.5 in) outer

diameter aluminium 6061-T6 cylindrical tubes having different length to diameter ratios by subjecting them to hydrostatic pressure in a pressure vessel. High speed 3D Digital Image Correlation (DIC) was used to track the deformation of the implodable. The pressure waves at selected locations around the implodable volume were recorded using dynamic tourmaline pressure sensors. When performing DIC on submerged objects, the effect of refraction due to the water body and any transparent window that is in the light path has to be accounted for [10,11]. Ke et al. [11] have described a rigorous method of accounting these effects in their work when the normal to window through which the object is viewed is inclined to the camera axes. In the present work, a set of calibration experiments were initially conducted to quantify the effects of submersion of the subject on the measured displacements before applying DIC to study the phenomenon of implosion.

The paper is organized as follows. In the next section, details of the experimental set up and the instrumentation used are discussed. Subsequently, calibration experiments performed to establish the use of Stereo-DIC to measure deformation of submerged objects are presented. This is followed by presentation of the experimental observations from the implosion experiments and the discussion on the results.

2. Experimental setup

2.1 Implosion experimental setup

2.1.1 Specimen geometry

The implodable volumes used in this study are commercially available aluminium 6061-T6 seamless tubes having an outer diameter of 38.1 mm (1.5 in) and a nominal wall thickness of 0.89 mm (0.035 in) providing diameter to thickness ratio of 42.9. These tubes are first inspected to identify any thickness variations along the length and circumference, and also for ovality in the cross section. This is important as imperfections such as thickness variation and ovality can affect the critical buckling pressure [12] and more importantly the location at which collapse will initiate. The tubes have a length to diameter ratio ($2L/D$) of 5.3, 8 and 10.7, where $2L$ is defined as the unsupported length, shown in figure 1(a). Such $2L/D$ ratio ensures mode-2 as the primary collapse shape in the implosion experiments [13,14]. As shown in figure 1(a), end caps made of the same material are fitted at the two ends of the tubes using o-rings to seal the tubes from water ingress during pressurization. The tubes are slightly compressed along a radius to create ovality so that the direction of mode-2 collapse can be anticipated reasonably for the purpose of placing the pressure sensors, and for real-time high speed stereo-photography. This radial compression was achieved by squeezing the tube between two parallel metal surfaces (vice-grips) just enough to create small ovality in tube. Table 1 shows geometric details of the specimens investigated in this study along with the experimental collapse pressures (P_c).

Figure 1 Details of the implodable volume (a) Schematic of the implodable volume and the end-cap and sensor locations (b) Implodable volume inside cage fixture (c) Suspended implodable volume inside pressure chamber. The location of all eight pressure sensors is shown using arrows

The implodable volume is supported in a cage structure as shown in figure 1(b). Dynamic Tourmaline pressure sensors (PCB 138A05) are placed on cross-sectional planes of the tube at select locations along the length of the tube (see figures 1(a) and 1(c)). Typically two sensors are used on each cross-sectional plane, one located on the radius along which collapse is anticipated (0° pressure sensor/flat side) and the second one on a radius orthogonal to the first one (90° pressure sensor/crease side). The sensors are placed at a radial distance of $4/3^{\text{rd}}$ tube diameter ($4D/3 = 50.8$ mm (2.00 in)) from the tube surface. The sensor planes are located at mid length, $3L/8$,

3L/4 and L from mid length along the axis of the cylinder as shown in figure 1(a). The signals from the pressure sensors are recorded using a data acquisition system (Astro-med Dash 8HF-HS, 200 KHz bandwidth) at a sampling rate of 2 MHz.

Specimen	Unsupported Length (2L)	Wall Thickness	Nominal Wall Eccentricity	Collapse Pressure (P_c)	Implosion Event Duration
2L/D = 5.3	203.2 mm (8.0 in)	0.889 mm (0.035 in)	2.16%	2.71 MPa (393 psi)	1.64 ms
2L/D = 8.0	304.8 mm (12.0 in)	0.889 mm (0.035 in)	2.43%	2.04 MPa (296 psi)	2.05 ms
2L/D = 10.7	406.4 mm (16.0 in)	0.889 mm (0.035 in)	2.22%	1.81 MPa (262 psi)	2.25 ms

Table 1 Details of the tube specimens used in this study

2.1.2 Pressure chamber facility

The steel pressure chamber facility is shown in figure 2. The chamber has a cylindrical section of 2.13 m (84 in) diameter and 1.07 m (42 in) length with hemispherical domes. A longitudinal section indicting the position of the implodable is shown in figure 2(a). The total height of the chamber (pole to pole) is 2.13 m (84 in). A section through the mid length of the chamber indicating the exact location of the view ports and cameras is shown in figure 2(b). The cylindrical segment of the chamber has eight circular view ports each having a 76 mm (3 in) thick clear acrylic window of diameter 102 mm (4 in) for viewing and illumination (see figure 2(b)). The chamber is pressurized using compressed nitrogen gas from top of the chamber using a solenoid inlet valve. On the onset of implosion, the volume of the specimen decreases leading to a small change in the hydrostatic pressure in the surrounding water.

Expansion of nitrogen gas on the top is desirable in order to simulate a submarine environment since it aids in maintaining a constant hydrostatic pressure in the pressure chamber.

A pair of Photran SA-1 cameras are used to obtain stereo images of the implosion process in real-time at 30,000 frames/second. A random intensity pattern is applied on the surface of the implodable using flat paint (see figures 1(b) and 1(c)) and the pattern is illuminated using a pair of high intensity arc lamps (as shown in figure 2(b)).

Figure 2 Pressure chamber facility at the University of Rhode Island (a) Longitudinal section (b) Section through mid length

2.2 3D DIC calibration setup

As noted in previous publications [11], determining accurate 3D positions for specimens submerged in a fluid environment and viewed through a single window requires a two-step calibration process; one using a grid in air and another using a grid submerged in the fluid and viewed through a single window. In this application, two windows are employed and the submerged specimen is viewed through both windows. Conceptually, the optimization approach outlined in the previous publications can be extended to calibrate the stereo-imaging system using two windows. However, results from these studies suggest that it may be possible when viewing perpendicular to the window to calibrate each camera (with modified imaging parameters) as if it were viewing a specimen through a single transparent medium. To evaluate the accuracy of this modified 3D DIC methodology for determining 3D positions for points on a submerged object, calibration experiments are conducted by imaging a submerged calibration specimen within a small custom designed tank. This technique replicates the optical effects of submersion using a more accessible smaller tank. A schematic of the setup is shown in figure 3. The setup is approximately 600 mm (24 in) long and 350 mm (14 in) wide with height of 150 mm (6 in). Two acrylic windows, of same thickness to those used in the pressure chamber facility are installed with a stereo angle of $\sim 20^\circ$ for viewing purpose. A 76 mm x 51 mm (3 in x 2 in) speckled flat aluminium specimen is mounted inside the tank on a precision translation stage, which can provide both in-plane and out-of-plane translation with 0.01 mm accuracy.⁶⁶

Figure 3 Small tank setup for underwater 3D DIC calibration

The SA-1 high speed cameras, with the same front end optics as will be used in the actual experiment, are utilized to capture image of the translated specimen during calibration experiments. Consistent with the implosion experiments, the cameras are placed outside the tank such that the surface normal of each window is aligned with the optical axis of the respective camera. The resolution of the camera image is 1024 x 1024 pixels, corresponding to an approximate magnification factor of 11.44 pixels/mm. Calibration of intrinsic and extrinsic parameters is performed using a submerged calibration grid provided by Correlated Solutions, Inc. [15]. The specimen is placed at the intersection point of the two camera axes inside the tank, a process that is consistent with future implosion experiments. Using the translation stage, the specimen is translated in 1 mm increments in both in-plane and out-of-plane direction and the corresponding images are captured. Displacement of the specimen at each

translation increment is estimated with Vic-3D software [15] using 51 x 51 pixel subsets and a step size of 7.

3. Experimental results and discussion

3.1 3D DIC calibration results

The out-of-plane 3D DIC calibration results for submerged objects are shown in figure 4. Both displacements and strains (von-Mises) are calculated from the 3D DIC measurements. To quantify the accuracy of the measured displacement and strains, the average surface displacement and standard deviation over the whole area for each given displacement is calculated. Similarly, the average strain value (pseudo strain as rigid translation is not supposed to produce any strains) and its standard deviation are also calculated. A plot of the actual out-of-plane displacement and the measured average displacement of the specimen is shown in figure 4(a). The measured average surface displacements are in good agreement with the true displacements and the percentage error between true and measured out-of-plane displacement is $\sim 1.5\%$ to 2.5% . The magnitude of absolute error is nearly proportional to the given displacement value, which results in an approximately constant percentage error. Strains are found to be $\sim 0.1\%$ to 0.3% in general (see figure 4(b)), which is an order of magnitude larger than the strain accuracy of the 3D DIC technique ($\sim 0.015\%$ - 0.02% [16]) in air. Hence, it can be concluded that the out-of-plane displacements can be accurately captured (within 3% error). The accuracy of the measured strains ($<$

0.3%) is adequate when large deformations are of interest, but may not be sufficiently accurate to identify strain trends during the early stages of the implosion event¹.

Figure 4 DIC calibration results for out-of-plane translation (a) Out-of-plane displacement and measurement error estimates (b) Measured von-Mises (pseudo) strain during out-of-plane translation

The in-plane 3D DIC calibration results for submerged objects are shown in figure 5. A comparison of given in-plane displacement and measured average DIC displacement as shown in figure 5(a) indicates that the two are in very good agreement. The absolute magnitude of error is nearly constant ~ 0.02 to 0.03 mm for the range of given displacement and therefore the percentage error shown in figure 5(a) decreases (from 1.2% to 0.5%) with increase in displacement. Strains are $\sim 0.1\%$ to 0.3% , which is similar to the case of out-of-plane translation. Therefore, when the optical axis of cameras is aligned with the normal of the viewing windows, both in-plane and out-of-plane displacements can be measured with good accuracy for

¹ It is expected that the motions of the structure during implosion will be on the order of 10mm in the normal direction and less than 10mm in the two orthogonal directions. If much larger motions (e.g., 100mm) occur, then degraded accuracy would be expected in the measurements since the calibration volume would not encompass the measurement region and the approximations embodied in the approach would be less accurate.

submerged objects by using the camera and stereo parameters obtained using a submerged calibration grid.

Figure 5 DIC calibration results for in-plane translation (a) In-plane displacement and measurement error estimates (b) Measured von-Mises (pseudo) strain during in-plane translation

Due to the difficulty of maintaining both the relative camera positions and the lens parameters for the two SA-1 cameras when moving to the pressure chamber facility, the authors re-calibrated the two cameras. After positioning each camera perpendicular to its viewing window of pressure chamber, the re-calibration process consisted of viewing a submerged calibration grid (size = 12 dots x 9 dots, interspacing = 12 mm) in several orientations. Here, the location of the grid corresponded approximately to the position where the implodable volume would be located. Once the calibration process was completed, the accuracy of 3D DIC displacement measurements in the pressure chamber facility was re-estimated by imaging one of the implodable volumes and using the images with 3D-DIC to quantify the radius of the implodable volume. After reconstructing the cylindrical shape of the implodable volume using Vic-3D software [15], the local radius of curvature of the specimen surface was estimated (see figure 6) using data from the central region of the

specimen. The measured radius varied between 18.83 mm and 19.03 mm, with the mean radius equal to 18.92 mm. Independent measurements of the true radius of the specimen gave a mean radius of 19.05 mm, which differs by *less than 1%*. Thus, measurements indicate that both the shape and 3D deformation of submerged objects can be measured accurately using the modified stereo-imaging calibration process with cameras viewing normal to the windows.

Figure 6 Measurement of radius using 3D DIC on $2L/D = 8$ implodable volume

3.2 Results of implosion experiments

3.2.1 Pressure history and DIC results for $2L/D = 8$

The real time out-of-plane deformation and velocity contours for $2L/D = 8$ are shown in figure 7. The deformation and pressure measurements are also obtained for $2L/D = 5.3$ and 10.7 , but only those for $2L/D = 8$ specimen are discussed in this section for brevity. Please note that the generation of first sharp pressure spike (will be discussed later in this section) at the nearest sensor location (M-0) is chosen as time $t = 0$ ms. The collapse pressure for $2L/D = 8$ is 2.04 MPa (296 psi) and the tube collapses in a mode-2 shape forming two lobes. The over-pressure observed at different Z locations for both orientations (0° and 90°) is plotted in figure 8. A plot of out-of-plane wall velocity history (measured at minor radius using DIC) at the Z

location of sensor is also shown in figure 8. The deformation of the specimen begins at the centre and is symmetric about $Z = 0$ in the length direction. The initiation of the implosion process as a mode-2 shape begins as a ‘slow ovalization’, well before the rapid collapse initiates (see $t = -5.9$ ms and -1.93 ms in figure 7). Although the velocity of the structure is negligible in this process, leading to no change in dynamic pressure in the near-field, it introduces a small change in the minor radius (~ 1.8 mm at centre, 10% of the radius) of the specimen (see $t = -1.93$ ms in figure 7). This deformation varies almost linearly along the length of the specimen up to the end-caps. Similar linear variation is observed for even larger $2L/D$ ratio of 10.7, which indicates that the whole structure undergoes mode-2 deformation during the implosion process.

Inspection of the results shown in figure 8 indicate that the walls of the specimen start accelerating by time $t = -0.93$ ms and the pressure at sensors M-0, M-90, L1-0 and L1-90 (figure 8) starts gradually decreasing. As the bulk modulus of water is very high (~ 2.2 GPa), any small increase in the volume of the compressed water due to deformation of the implodable (resulting in decrease in the density of water) near the specimen, results in a drop in pressure. This local pressure depreciation causes the generation of low pressure waves propagating outwards. As the central part of the implodable undergoes maximum deformation, the intensity of the generated low pressure wave is stronger at the mid-span of the implodable. Also, the intensity of such waves decreases with increasing radial distance because of spherical/cylindrical decay of the pressure wave [3,8].

Figure 7 Measured out-of-plane displacement and velocity using 3D DIC for $2L/D = 8$ implodable volume. Specimen moving out-of-plane towards the axis of the cylinder is defined as positive deformation.

At $t = -0.93$ ms, pressure at sensor M-0 reaches a value of -0.046 MPa, a small drop in dynamic pressure, which can be correlated to the initiation of wall recession at the centre of the specimen (see DIC results at -0.93 ms). This deformation grows rapidly along the entire length, causing a simultaneous decrease in dynamic pressure. At $t = -0.57$ ms, more than half of the length of the specimen experiences a wall velocity of ~ 4 m/s, leading to pressure decrease even at sensors L2-0 and L2-90. At $t = -0.57$ ms, the pressure at M-0 and L2-0 reaches half of minimum pressure ($0.5 P_{\min} = -0.24$ MPa) and $1/4^{\text{th}}$ of $P_{\min}$ (-0.12 MPa) respectively. By time $t = -0.17$ ms, the

Figure 8 Pressure and out-of-plane wall velocity history for $2L/D = 8$ implodable volume. Wall velocity is plotted at corresponding sensor's Z -location at the minor radius. Numbered dots in pressure history correspond to images in figure 7. (a) Pressure and wall velocity observed at $Z = 0$ (b) Pressure and wall velocity observed at $Z = 3L/8$ (c) Pressure and wall velocity observed at $Z = 3L/4$

pressure reaches a minimum of -0.49 MPa at M-0. At this instant, DIC velocity contours show that the centre of the implodable reaches its maximum wall velocity (~ 25 m/s), which leads to the largest rate of change in volume of the implodable. As water is following the implodable's surface, the local rate of decrease in density of water simultaneously reaches its peak, causing the lowest pressure in the water. Note that the lowest pressure is approximately maintained with a marginal increase for 0.17 ms at M-0 until the first pressure spike is observed.

The first sharp pressure spike observed at time $t = 0$ ms (shown in inset in figure 8) has a rise time of ~ 4 μ s at M-0, which is similar to the characteristic rise time of the UNDEX shock wave in water. The change in pressure due to this peak pulse is 0.78 MPa (starting at -0.46 MPa, ending at 0.32 MPa) with a duration of ~ 10 μ s. By comparing this phenomenon with the measured deformation history, the first pressure spike seems to originate from the initiation of wall-to-wall contact at the centre of the collapsing implodable volume, as seen from the velocity contour at time $t = 0$ ms. The contact of the two opposite walls moving towards each other at a relative velocity of ~ 50 m/s acts as an impact between two solid surfaces and leads to strong sound waves in the tube, which gets transferred to water and manifests as a very short duration shock pulse. This first pressure shock wave is primarily seen at sensor for 0° direction. Since the spike does not seem to be present at sensors located at 90° (M-90, L1-90), it can be inferred that the first pressure spike results in a shock wave emerging mainly along the minor diameter direction (0°).

Continuing with sensor M-0, the pressure keeps decreasing for ~ 50 μ s after the occurrence of the first pressure spike, indicating that after the first wall contact at

the centre, the receding walls transition from a point contact at the centre to a line contact along the major diameter (see time $t = 0.07$ ms in figure 7, where the contact at the centre is spreading along the diameter but still under transition). After $t = 0.07$ ms, the pressure increases monotonically due to a rapid increase in the contact area between the walls of specimen. This event leads to strong deceleration in the water that is moving along with the receding walls, resulting in an increasing mass of water that is impacting the surface of the specimen. The impact of water causes the incoming momentum to vanish, causing high pressure build-up at the surface of the implodable. Finally, by $t = 0.17$ ms, pressure at M-0 reaches the highest value of 1.90 MPa. The DIC results at this instant show that the contact has spread along the major diameter of the specimen at the centre and $\sim 30\%$ of the length of specimen has made wall contact. Such large peaks have been observed previously by several researchers [1–3,8]. Turner and Ambrico [3] observed through numerical simulations that this large pressure spike occurs around the time when the width at the centre of the implodable exhibits contact, similar to the experimental results shown here. Comparing the magnitudes of peak pressure observed at other 0° sensors, the peak overpressure was 1.37 MPa at L1-0 and 0.58 MPa at L2-0.

After the occurrence of the large pressure spike, pressure rapidly decreases with a decay time of ~ 100 μ s indicating the transient nature of contact propagation. The rate of increase in contact surface area after the initiation of initial point contact is large (leading to high peak pressure), but it also decreases rapidly. Later in time, from $t = 0.17$ ms to 0.60 ms, the contact front starts propagating towards the direction of end-cap with high velocity (~ 100 -200 m/s, i.e. buckle propagation velocity [17]). The

propagation of collapse along the length starts as a transition from line contact at the centre, in which the contact front angle is $\sim 90^\circ$ at $t = 0.17$ ms. It transforms into a steady plane collapse front from time $t = 0.17$ ms to 0.30 ms. Interestingly, the highest local wall velocity during collapse occurs during the propagation of buckles and is found to be present ahead of the collapse front at 2 symmetric points about the central Z axis (see $t = 0.17$ ms, 0.30 ms and 0.40 ms in figure 7). Moreover during propagation of buckles, the magnitude of the pressure waves radiated is relatively small. The pressure at all the sensors during this phase is also fairly similar and is less than 0.25 MPa.

3.2.2 Buckle propagation and contact zone

The application of the 3D DIC technique allows for real-time deformation/velocity measurements during the evolution of mode-2 collapse, both up to initial contact and also during the buckle propagation from the mid length towards the end caps. The propagation of buckles along long submarine pipelines is a well-studied phenomenon [17–19]. Kyriakides and Netto [17] reported that the buckle propagation velocity is dependent on the pressurizing medium; a buckle running in an air pressure chamber will run faster than that in a water pressure vessel. We believe that this is the first-ever reporting of real-time quantitative visual observation of propagating buckles in underwater implosions. After contact initiation at mid length ($t = 0$ ms in figure 7), the contact area of the implodable volume grows rapidly, with a sharp acute angle shape propagating along the tube length (see $t = 0.07$ ms in figure 7). The buckle is still in the transition phase, where a local initial collapse is transforming into a running buckle. The running buckle rapidly grows along the Z -direction, and

the cone angle increases to approximately 90° by time $t = 0.17$ ms. Later, it reaches a stable propagation velocity while growing steadily along the Z -direction. A typical plot of out-of-plane wall velocity profile observed for $2L/D = 8$ along the centre line ($\theta = 0^\circ$) is shown in figure 9(a). It must be noted that, after reaching the maximum velocity of 25 m/s at the centre, the velocity near the centre diminishes abruptly with high decelerations, even before point contact initiates. During this time, the rest of the specimen is still collapsing with collapse velocity. The rapid velocity drop from 25 m/s to 4 m/s takes $\sim 100 \mu\text{s}$, occurring just before the small pressure spike due to contact. Comparison of velocity history at the centre point (shown in figure 9(b)) shows the same behaviour for $2L/D = 8$ and 10.7. For $2L/D = 5.7$, there is slightly higher peak velocity ($\sim 20\%$ more), probably due to relatively higher collapse pressure.

By measuring the time-varying length of the contact front at the minor diameter ($\theta = 0^\circ$) for all the specimens, the calculated contact front velocity U_{exp} (or the buckle propagation velocity) as a function of time is plotted in figure 9(c). During the initial stages of contact at the centre, the propagation velocity of contact front is very high (~ 350 m/s to 600 m/s), then later, the contact propagates stably with a velocity between 100 m/s to 200 m/s. Note that the initial contact front velocity is highest for the largest $2L/d$ ratio ($= 10.7$) due to the longer unsupported length for large $2L/D$ ratio (reduced flexural stiffness). Earlier studies on the buckle propagation by Kyriakides and Netto [17] have shown that the ratio of steady-state buckle propagation velocity (U) and $\sqrt{\sigma_0/\rho_t}$ depends upon the ratio of critical pressure (P_c) and the propagation pressure (P_p), where σ_0 is the flow stress and ρ_t is the density of the material. For our experiments, the value of P_p , P_c/P_p and U are

calculated from [17] and shown in Table 2. When comparing U with U_{exp} (shown in figure 9(c)), U_{exp} is always smaller than U during stable buckle propagation, which suggests that $2L/D$ ratio of tubes chosen in this study is still smaller as compared to tubes used for calculating U . The smaller $2L/D$ ratio causes end-cap (end boundary condition) to play significant role during buckle propagation from the beginning of collapse. The increased flexural stiffness provided by end-caps is shown to reduce the buckle propagation velocity by more than 50% (for the case of $2L/D = 5.3$ and 8) in this study.

Figure 9 Analysis of contact propagation for all $2L/D$ ratios using measured DIC velocity contours. (a) Measured out-of-plane velocity profile from DIC for $2L/D = 8$ (b) Mid-point velocity history measured along the minor radius ($\theta = 0^\circ$) at the mid length ($Z/L = 0$), velocity history for each specimen is shifted in y direction for clarity (c) Contact propagation velocity measured along Z axis at minor radius ($\theta = 0^\circ$) for all specimens. (d) Contact area and the rate of contact area change for all specimens

Specimen	P_c	P_p	P_c/P_p	$U/\sqrt{\sigma_0/\rho_t}$ from [17]	U
2L/D = 5.3	2.71 MPa	0.59 MPa	4.59	1.2	384 m/s
2L/D = 8.0	2.04 MPa	0.59 MPa	3.45	1.0	320 m/s
2L/D = 10.7	1.81 MPa	0.59 MPa	3.06	0.8	256 m/s

Table 2 Calculation of propagation pressure (P_p) and steady state buckle propagation velocity (U) from [17], $\sigma_0 = 276$ MPa, $\rho_t = 2700$ kg/m³

After the initiation of contact, the dynamics of contact zone growth in the implodable is important in the evolution of high pressure waves. Therefore the area of contact zone is calculated by estimating the zero velocity region from the 3D-DIC full-field results. The plot of contact zone area and rate of increase in area is shown in figure 9(d). The rate of increase in contact area reaches a maximum value at ~ 150 μ s for $2L/D = 8$ and 190 μ s for $2L/D = 10.7$. It is interesting to note that ~ 10 μ s later, the largest pressure spike occurs at ~ 160 μ s for $2L/D = 8$ and 200 μ s for $2L/D = 10.7$. Thus the highest pressure spike is the result of a sudden increase in the contact area growth rate, causing the largest change in momentum of water against the wall, eventually leading to the highest pressure spike. Turner and Ambrico [3] suggested that the highest pressure spike corresponds to the time when collapse in the implodable volume extends to the complete width at the centre. Since both the highest rate of increase in contact area and also the extension of collapse along the width in cylindrical tubes occur at the same time, and therefore, both arguments can explain the occurrence of the highest pressure. However, for an implodable volume with an arbitrary shape which is distinctly different from a cylindrical shape, the contact area growth rate is a better parameter to relate the time and magnitude of the largest pressure spike with the collapse behaviour.

3.2.3 Positive time period (T_+)

The initiation of contact and buckle propagation towards the end caps produces high amplitude pressure pulses along with low magnitude, steady pressure pulses for long time durations. The time duration of a high pressure wave (which will be referred as positive time period, T_+) is crucial for understanding the overall strength of the implosion generated shock wave. From the experiments in this study, T_+ was observed to change depending upon the $2L/D$ ratio or the length of the specimen (see figure 10). Ideally, T_+ should be equal to the sum of (a) the time it takes for the initial buckle to move from the mid length of the implodable to the end caps and (b) the time it takes for the wave to reach the sensor location. Therefore T_+ can be written as

$$T_+ = \frac{L}{u_{buckle-avg}} + \frac{\Delta r_2 - \Delta r_1}{c_0} \quad (1)$$

where $u_{buckle-avg}$ = average buckle propagation velocity for the specimen; c_0 = wave speed in water (1482 m/s in this study); Δr_1 = distance between the centre of the specimen and the sensor; Δr_2 = distance from the end caps to the sensor.

At a given Z/L , there is a significant increase in T_+ (~ 0.44 ms at mid length) with increasing $2L/D$ ratio. As the impulse of the positive pressure pulse depends upon both the pressure magnitude and time period, the impulse can be enhanced with increasing length of the implodable volume. This poses a larger risk of damage to nearby structures when longer implodable volumes are used in design.

Figure 10 Plot of positive time period (T_+) versus Z / L

For the sensors near the mid length, $\Delta r_2 - \Delta r_1 > 0$. On the contrary, the sensors near the end-caps have $\Delta r_2 - \Delta r_1 < 0$. Therefore, T_+ is higher for the sensor at the mid length (M-0, M-90) and smaller for the sensors at the end-caps (L3-90). This aspect can also be explained by the temporal stretching of the pressure wave ($T_+ \uparrow$), when the buckle is moving away from the sensor. Moreover, the movement of the buckle towards the sensor location causes pressure waves to compress leading to smaller time period of the pressure wave ($T_+ \downarrow$). A plot of T_+ with respect to normalized location of the sensor from Eq. (1) is plotted as dashed line in figure 10 and $u_{buckle-avg}$ is chosen as a fitting parameter to match experimentally observed T_+ values. As the diameter is constant across all the specimens, the higher unsupported length of the implodable inherently decreases the flexural stiffness to deform in mode-2 after the point contact. This causes a higher buckle propagation velocity in the early transition phase, resulting in a higher “average” buckle propagation velocity. Hence the $u_{buckle-avg}$ (calculated from Eq. (1)) increases from 157 m/s to 205 m/s with increasing L/D ratio

from 5.3 to 10.7. Also, such estimation of $u_{buckle-avg}$ is made using only the pressure history and thus these values are compared with average buckle propagation velocity from DIC measurements (from Sec. 3.2.2) in Table 3. As shown in Table 3, there is good agreement between the two measured velocities ($u_{buckle-avg}$) indicating that Eq. (1) is an accurate estimate for measuring average buckle propagation velocity.

Specimen	$u_{buckle-avg}$ (Pressure) using Eq. (1)	$u_{buckle-avg}$ (DIC)	Difference (%)
2L/D = 5.3	157 m/s	165 m/s	4.8%
2L/D = 8.0	190 m/s	166 m/s	14.4%
2L/D = 10.7	205 m/s	207 m/s	0.97%

Table 3 Average buckle propagation velocity ($u_{buckle-avg}$) from pressure and DIC measurements

3.2.4 Impulse

The high pressure peak, along with the corresponding impulse, defines the strength of the shock wave. The impulses calculated from the over-pressure history during the pressure trough (under-pressure region) and high pressure wave (over-pressure region) are defined as I_- and I_+ respectively [8]. A plot of I_- and I_+ and the ratio $|I_+/I_-|$ for all the implodables studied is shown in figure 11. For the implodable volumes chosen, a significant magnitude of impulse (both I_- and I_+) is generated during the mode-2 implosion. Consistent with earlier observations of Turner and Ambrico [3,7], and Farhat et al. [8], I_- and I_+ are very similar in this study for most of

the implodables (I_+ is within 95% of I_- for 2L/D ratio of 5.3 and 8). The maximum impulse is always observed at the centre and the larger 2L/D ratio implodable volumes produce higher I_- . Also, the value of I_- near end-caps is approximately same for all the cases. This indicates that I_- is independent of 2L/D ratio when measured near the end-caps. It is important to note that the experiments for 2L/D = 10.7 do not exhibit a good match between I_- and I_+ . The values of I_+ are consistently lower at all Z locations for this implodable (as low as 70% of I_- at certain Z/L locations). Authors believe that such inconsistencies between I_- and I_+ are not the experimental errors but rather are the effect of reflected waves due to the finite size of the pressure chamber facility. These differences between I_- and I_+ can arise in experiments, in which the total duration of the implosion event (which includes both under-pressure phase and over-pressure phase) is much longer than the reflection-free time window of the pressure chamber (which is 1.4 ms in this study). As the duration of total implosion event is largest (2.25 ms) for the longest implodable volume 2L/D = 10.7 (see Table 1), the last 0.85 ms of the over-pressure phase in pressure history is not reflection-free. The pressure recorded in this time duration is a result of superposition of two waves: 1) over-pressure implosion waves radiating outwards, and 2) reflected pressure release waves from the solid pressure chamber walls, which move radially inwards. As the under-pressure waves released outwards during implosion (all waves released prior to time $t = 0$ ms) reflect with same sign from the solid boundary of the pressure chamber and converge back towards the centre, the pressure history near the

centre tends to decrease. Such decrease in pressure results into lower magnitude of I_+ as compared to I_- .

Figure 11 Evaluation of negative and positive pressure impulse for all specimens (a) Variation of negative pressure impulse (I_-) versus Z/L (b) Variation of positive pressure impulse (I_+) versus Z/L (c) Ratio of positive and negative pressure impulse ($|I_+/I_-|$) versus Z/L

The values of I_- and I_+ at each sensor location are similar in all the implosion experiments (if reflections from the boundaries don't interfere within the implosion

event). The explanation for vanishing $\int_{T_-}^{T_+} P(t)dt$ (or $I_- = I_+$) lies in the inherent nature

of the implosion (here $t = T_-$ refers to start of the under-pressure phase or the

beginning of dynamic implosion process). An implosion process consists of two

phases: 1) the under-pressure phase when the pressure in nearby water decreases and water rushes towards the specimen (radially inwards), and 2) the over-pressure phase when wall contact initiates in the implodable and the velocity of water decreases. The inward moving water in the under-pressure phase must reach zero velocity after the implosion event is over. If free-field conditions are assumed (i.e. outward generated implosion waves will never return) and no other pressure waves are generated at the surface of the implodable after T_+ (or the end of implosion process), the water particle will reach zero velocity only by the over-pressure pulse released by the implodable itself. Therefore, the released high pressure wave should have sufficient positive impulse to make the inward water particle velocity vanish at all locations. This results into I_+ being equal to I_- . This process can be mathematically described by the use of over-pressure ($P(t)$) and particle velocity ($u(t)$) relationships from [20,21]. For a spherical acoustic pressure wave, the particle velocity, $u(t)$, at any time t and arbitrary radius R can be written as [20]:

$$u(t) = \frac{P(t)}{\rho c_0} + \frac{1}{\rho R} \int_0^t P(t) dt \quad (2)$$

where ρ is the density of water (998.2 kg/m³). As $u(t)$ is zero at the start of implosion process ($t = T_-$) and must vanish at the end of implosion ($t = T_+$), it implies (from Eq. (2)) that:

$$u(t = T_+) = 0 \Rightarrow \frac{P(t = T_+)}{\rho c_0} + \frac{1}{\rho R} \int_{T_-}^{T_+} P(t) dt = 0 \quad (3)$$

since $P(t = T_+) = 0$

$$\Rightarrow \int_{T_-}^{T_+} P(t) dt = 0 \quad (4)$$

Thus, as long as the waves generated in implosion can be approximated as spherical

(similar to conclusion of Turner and Ambrico [3]), $\int_{T_-}^{T_+} P(t) dt$ must vanish.

4. Conclusion

A comprehensive series of experiments is conducted to study the underwater free-field implosion of cylindrical shells using the 3D DIC technique. A simple calibration procedure for the application of the 3D DIC technique for submerged objects is presented. The experiments are conducted with varying length to diameter ratio of the implodable. The near-field pressure history measurements are synchronized with DIC full field deformation of the implodable volume during implosion. The key findings of this study are as follows:

- 3D DIC technique can be successfully utilized for displacement measurements for submerged objects by extracting intrinsic and extrinsic parameters using a submerged calibration grid given that the camera's axes are aligned with the surface normal of the window. The error of measured DIC displacement is < 3% for out-of-plane motion and < 1.2% for in-plane motion.
- There exists a large degree of pre-deformation (up to 10% of the radius) in the cylindrical shells prior to any dynamic pressure fluctuations in the near-field. This pre-deformation varies linearly along the implodable length and occurs relatively slowly over the entire length of the implodable.

- After the initiation of the point contact, the contact front moves with very high velocities (~ 350 m/s to 600 m/s) before reaching a stable buckle propagation velocity between ~ 100 m/s and 200 m/s in this study. The estimation for average buckle propagation velocity can also be made by relating this velocity to the positive time period of the emitted high pressure waves.
- The rate of growth in contact area during an implosion has significant influence on the generation of a high pressure spike. The high rate of increase in contact area leads to large changes in the momentum of water following the walls and this causes a large spike in the pressure.
- The rate of growth in contact area is influenced by both the end-cap boundary conditions and the deformation mechanics of the implodable.
- For a given diameter, longer implodable volumes generate higher pressure spikes. It is believed that as the length increases, this effect will stabilize.

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CHAPTER 3

STUDY OF DYNAMIC UNDERWATER IMPLOSION IN AN OPEN-ENDED CONFINING TUBE USING 3-D DIGITAL IMAGE CORRELATION

by

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Abstract

An experimental study is conducted to investigate the fluid structure interaction phenomenon occurring during a confined implosion process using high-speed 3-D digital image correlation (DIC). Aluminum 6061-T6 implodable volumes with identical diameter and length with varying collapse pressures are placed inside a confining tube with one end open to the pressure vessel environment and are hydrostatically loaded for the natural implosion. The implosion event is viewed through the cylindrical acrylic window section of confining tube. Full-field deformation/velocities captured through 3-D DIC are synchronized with the pressure history to understand the effect of confining environment on the implosion process. Experiments show that implosion of the implodable volume (collapse pressure = P_c) inside the confining tube leads to strong oscillating water hammer waves with $1.35P_c$ to $1.92P_c$ magnitude. Study also reveals that the varying collapse pressure leads to faster implosions. Both peak and average structural velocities increase linearly with increasing collapse pressure. The effect of confining environment is most promptly seen in relatively lower collapse pressure implosion experiments which indicate a long deceleration phase following the peak velocity until wall contact initiates. The duration of this deceleration phase decreases with increasing collapse pressure.

Keywords

Implodable Volume, Collapse, Pressure Waves, Fluid Structure Interaction, Water Hammer, Confining Environment, 3-D Digital Image Correlation, High-speed Photography

1. Introduction

The buckling of cylindrical shell structures has been investigated extensively because of their application in the design of underwater and aerospace structures. These structures undergo extreme external pressures when used in underwater applications. If the external hydrostatic pressure exceeds a certain value for a given design, the structure loses its structural stability and undergoes buckling. This buckling in underwater situations is a rapid process and causes the entire structure to collapse onto itself. This event commonly referred as “implosion” is shown to be highly violent in nature with resulting high velocity water motion, strong shock waves and sound [1]. Several investigations have been reported by researchers in naval and marine communities on the mechanics and fluid-structure interaction during a free-field implosion process [2–13]. From the study conducted by Turner and Ambrico [8], the mechanism of implosion process for metallic structures can be described as follows: (1) the initial collapse phase, prior to wall contact, is accompanied by a smooth decrease in pressure in the surrounding water, (2) at the moment that contact is made between opposing sides of the collapsing cylinder at the center, a short duration pressure spike is emitted in the surrounding water, (3) a large positive pressure is produced at the instant that contact between the two opposing sides extends the full width of the cylinder, and (4) as the buckle propagates toward the ends, the pressure pulse continues, but at a lower magnitude, until the buckle reaches the end cap and the collapse of the cylinder completes.

Although the mechanics of implosion is well established for free-field implosion situations, the studies reporting the implosion occurring in confining

environments are very limited. The authors have recently reported the mechanics of implosion of cylindrical shells in a closed confining environment [14, 15]. The result of these studies indicated that the limited hydrostatic potential energy present in water significantly affects the implosion process in confining environment. The rate and extent of collapse of implodable volume dramatically reduces due to sudden decrease of potential energy inside the confining tube, and the magnitude of pressure hammer wave is always smaller than the hydrostatic pressure. If the confining tube is open at one end, the mechanics of implosion changes drastically and it leads to generation of extremely strong water hammer waves with significant time period [16]. Author's recent study on sympathetic implosion inside the open ended confining tube also shows that these hammer waves can potentially damage even relatively stronger implodable volumes inside the confining tube [17]. Both of these studies measured the dynamic pressure history inside the open ended confining tube to understand the evolution of water hammer waves at the onset of implosion. As the evolution of such implosion waves is a highly fluid structure interaction process, the structure coupled with the surrounding fluid leads to the generation of water hammer waves. Any changes in the design of the structure will cause to alter the fluid structure interaction process and so the strength of hammer waves. Therefore, in a real design, the mechanics of collapse can be completely different depending upon the geometry/location of the implodable volume in the confining tube. Thus, there is a need to understand the evolution of these waves from both structural deformation and the fluid mechanics point of view in order to predict the peak strength/impulse of these detrimental water hammer waves in real situations. To the best of author's knowledge,

there have been no studies reported on relating the generation of water hammer waves with the structural deformations of the implodable volume. Hence, this article addresses this research gap by investigating the underwater implosion mechanics in the open ended confining tube using 3-D digital image correlation. The implosion experiments are conducted with 31.8 mm outer diameter and 254 mm long implodable volumes with four different wall thicknesses such that the collapse pressure varies from 1.50 MPa to 4.24 MPa. This variation in critical collapse pressure allows identifying the changes in open ended confining tube's implosion mechanics with increasing collapse pressure. Results of this study show that an increase in collapse pressure increases the structural velocity significantly leading to a faster implosion process. This in turn results into higher entering water velocity at the open end of the confining tube, which generates stronger water hammer waves for relatively higher collapse pressures. Contrary to a free-field implosion process in which the structural velocity is highest right before the initiation of wall contact [10], the confined open tube implosion shows that the structural velocity reaches to a peak value well before the wall contact initiation. This is followed by a deceleration phase until the initiation of wall contact.

2. Experimental setup

2.1 Implodable Volume and Confining Tube

The implosion experiments are conducted inside an underwater pressure vessel facility at the University of Rhode Island. The implodable volumes chosen in this study are made out of commercially available aluminum 6061-T6 seamless extruded

tubing with a nominal outer diameter of 38.1 mm (1.5 in) and an unsupported length of 254 mm (10.0 in). The wall thickness of the implodable volume is increased from 0.73 mm (0.029 in) to 1.09 mm (0.043 in) in order to achieve a decent variation in the collapse pressure. Table 1 provides a brief layout of the implosion experiments conducted in this study. The implodable volumes are sealed on both ends using solid aluminum end-caps, which utilize circumferential o-rings for sealing the specimen for underwater experiments.

Experiment Label	Unsupported Length (l)	Outer Diameter (d)	Wall Thickness (t)	Experimental Collapse Pressure (P_c)
EXP-W29	254 mm (10.0 in)	38.1 mm (1.500 in)	0.73 mm (0.0287 in)	1.50 MPa (218 psi)
EXP-W35		38.1 mm (1.500 in)	0.89 mm (0.0349 in)	2.05 MPa (297 psi)
EXP-W38		37.5 mm (1.478 in)	0.98 mm (0.0385 in)	3.14 MPa (456 psi)
EXP-W43		37.8 mm (1.488 in)	1.10 mm (0.0432 in)	4.24 MPa (615 psi)

Table 1 Layout of the experiments

The implodable volumes are placed concentrically inside a confining tube with one open end which sits inside the underwater pressure vessel facility [10]. The schematic of pressure vessel facility with confining tube and implodable volume is shown in Fig. 1. The pressure vessel has a cylindrical section of 2.13 m (84 in) diameter and 1.07 m (42 in) length with hemispherical domes. A longitudinal section indicating the position of confining tube along with the implodable volume is shown in Fig. 1(a). The total internal height of the vessel is 2.13 m (84 in). A section through the mid length of the vessel indicating the exact location of the view ports and cameras is shown in Fig. 1(b). The cylindrical segment of the vessel has eight circular view

ports each having a 76 mm (3 in) thick clear acrylic window of diameter 102 mm (4 in) for viewing and illumination (see Fig. 1(b)). The pressure vessel is pressurized using compressed nitrogen gas from top of the chamber using a solenoid inlet valve. On the onset of implosion, the volume of the specimen decreases leading to a small change in the hydrostatic pressure in the surrounding water. Expansion of nitrogen gas on the top compensates for this change and aids in simulating a constant hydrostatic pressure free-field environment inside the pressure vessel.

The schematic of the one end open confining tube utilized in this study can also be seen in Fig. 1. The confining tube has an inner diameter (D) of 178 mm (7.0 in) with 25.4 mm (1.0 in) wall thickness and is made out of aluminum 6061-T6. The confining tube is equipped with a 152 mm long (6.0 in) cylindrical transparent acrylic window section in order to visualize the deformation of implodable volume during implosion event. The inner diameter and the wall thickness of cylindrical window section are identical as aluminum section to maintain constant cross-sectional area throughout the length in the confining tube. The total length (L) of the confining tube is 1.12 m (44 in). The bottom end of the confining tube is closed with an end-plate and the top end is open to the high pressure water environment of the pressure vessel. The implodable volumes are placed inside the confining tube such that the distance between the bottom end-plate and the center of the confining tube is 686 mm (27 in). Both high frequency dynamic face pressure sensors (PCB-113B22) and tourmaline blast pressure sensors (PCB 138A05) are installed in the inner wall of the confining tube at various locations to capture the water hammer wave evolution during the

implosion event. The signal from the pressure sensors is recorded using a 200 KHz bandwidth recorder at a sampling rate of 2 MHz.

Figure 1 Schematic of the open-ended confining tube implosion facility. (a) Longitudinal section of the underwater pressure vessel. The confining tube is placed at the center of the pressure vessel. (b) Section through the mid-length of the pressure vessel. A detailed schematic of open-ended confining tube indicating the exact sensor locations is shown on the right side of the figure inside dashed box.

The real time deformation of the implosion event is captured using a pair of Photron SA-1 high speed cameras at 30,000 frames/second. A random intensity

pattern is applied on the surface of the implodable volume using flat paint and the pattern is illuminated using a pair of high intensity arc lamps similar to [10].

2.2 3-D Digital Image Correlation Calibration Setup

Authors have recently shown that calibration of extrinsic and intrinsic parameters by using a submerged calibration target can result into high accuracy in both in-plane and out-of-plane displacement measurements using 3-D DIC [10]. As the experimental setup used in this article contains an additional medium in the optical path of the cameras (i.e. cylindrical acrylic window as shown in Fig. 1), the technique proposed in [10] requires recalibration in order to estimate the accuracy of measured DIC in-plane/out-of-plane displacements for objects placed inside a cylindrical window. Therefore, calibration experiments are conducted in a custom designed tank (shown in Fig. 2) to re-evaluate the accuracy of measured DIC displacements. This custom tank utilizes the identical cylindrical window section described in Sec. 2. Hence, this setup replicates the optical effect of submersion of implodable volume inside the cylindrical window section of the confining tube using a more accessible smaller tank. The setup is approximately 600 mm (24 in) long and 350 mm (14 in) wide with a height of ~150 mm (6 in). A 76 mm x 51 mm (3 in x 2 in) flat aluminum specimen is placed inside the cylindrical window on a precision translation stage, which can provide translations with 0.01 mm accuracy. The aluminum specimen is placed at approximately 20 mm in front of the central axis of the cylindrical window towards cameras, which represents a typical location of 38.1 mm outer diameter implodable volume inside the confining tube. A random high contrast black/white speckle pattern is painted on the front surface of the specimen for DIC measurements.

Figure 2 Custom designed tank setup for underwater DIC calibration experiments

The SA-1 high-speed cameras, with the same front-end optics, are used to capture images of the translated specimen during calibration experiments. The resolution of the camera image is 1024×1024 pixels, corresponding to an approximate magnification factor of 4.02 pixels/mm. Calibration of intrinsic and extrinsic parameters is performed using a submerged calibration grid (12 dots x 9 dots, 7 mm interspacing) provided by Correlated Solutions, Inc.[18]. Using the translation stage, the specimen is translated in 1 mm increments in both in-plane and out-of-plane directions and the corresponding images are captured. Displacement of the specimen at each translation increment is estimated with Vic-3D 2012 software using 45×45 pixels subsets and a step size of 7 [18].

3. Experimental Results and Discussion

3.1 Underwater 3-D DIC Calibration Results

The out-of-plane 3D DIC calibration results for submerged objects are shown in Fig. 3. Both displacements and strains (von-Mises) are calculated from the 3D DIC measurements. To quantify the accuracy of the measured displacement and strains, the average surface displacement and standard deviation over the whole area for each given displacement is calculated. A plot of the true out-of-plane displacement and the measured average DIC displacement of the specimen is shown in Fig. 3(a). The measured average DIC displacements are in good agreement with the true displacements and the percentage error between true and measured out-of-plane displacement is less than 5%. The range of pseudo strain is calculated across the area such that 95% of the values lie in the range. The value of pseudo strains is found to be increasing with increasing out-of-plane displacement and the maximum range of pseudo strains is found upto 0.5% for 10 mm displacement (see Fig. 3(b)). This monotonic increase in pseudo strains is possibly due to the effect of cylindrical acrylic window. The translation changes the effective distance between window and the aluminum target causing curvature of the window to distort the image leading to higher pseudo strain values for larger translations.

The in-plane 3D DIC calibration results for submerged objects are shown in Fig. 3(c). A comparison of true in-plane displacement and measured average DIC displacement as shown in Fig. 3(c) indicates that the two are in very good agreement. The magnitude of error is $< 1\%$ for all in-plane displacements. The range of pseudo

strains is found to be upto 0.4% (see Fig. 3(d)). As the measurement of displacements is the primary interest in the implosion experiments, it can be concluded that both in-plane and out-of-plane displacements can be measured with adequate accuracy using this modified DIC calibration in the case of submerged object viewed through a cylindrical acrylic window. The extraction of camera's intrinsic and extrinsic parameters using submerged calibration grid can successfully account for the change in refractive index due to water and both flat/cylindrical acrylic viewing window.

Figure 3 Underwater DIC calibration experiments (a) Out-of-plane displacement (b) measured von-Mises (pseudo) strain during out-of-plane translation (c) in-plane displacement (b) measured von-Mises (pseudo) strain during in-plane translation

For the implosion experiments, the accuracy of measured DIC displacement is further estimated quantifying the radius of the implodable volume using 3D DIC technique. This is performed by reconstructing the cylindrical shape of implodable volume and estimating the local radius of curvature across the implodable volume's

surface. A typical plot of radius of implodable volume is shown in Fig. 4. The measurement of radius agrees very well with the true radius of the implodable volume. The radius is found to $19.07 \text{ mm} \pm 0.22 \text{ mm}$ (with 95% confidence interval). As the true radius of the specimen is 19.05 mm, the maximum deviation from true radius is found to be 2.25%. Thus, it can be established that both the shape and 3-D deformation of submerged objects behind cylindrical window can be measured accurately using the modified calibration process described in Sec. 2.2.

Figure 4 Measurement of the radius of the implodable volume using 3-D DIC

3.2 Underwater Confined Implosion Results

3.2.1 Change in Full-field Structural Velocity as a Function of Collapse Pressure

The real time full-field high speed velocity contours for the lowest collapse pressure experiment (EXP-W29) and for the highest collapse pressure experiment (EXP-W43) is shown in Fig. 5. Note that 3-D DIC measurements are also conducted for other collapse pressures (EXP-W35 and EXP-38), but only the extreme ends of collapse pressure experiments are discussed in this section for brevity.

Figure 5 Full-field out-of-plane velocity contours for (a) EXP-W29 and (b) EXP-W43. Distance between M and M+/M- is equal to $l/4$.

Time $t = 0$ ms indicates the time at which the dynamic pressure at the nearest sensor (Ch-4) drops to 99% of the collapse pressure. The velocity contours for EXP-W43, as shown in Fig. 5(a) shows that the evolution of velocity is different from the free-field implosion experiments. The implodable volume accelerates upto time $t = 1.3$ ms and reaches a peak center point velocity of 12 m/s. During this period, the velocity variation along the length is almost linear similar to earlier studies [19]. The deformation process after this instance is completely different from free-field situations. The implodable volume undergoes deceleration at the center for about 1.5 ms, while the points along the longitudinal length gains velocity as seen in $t = 1.5 - 2.5$ ms. This is followed by a point contact initiation at the center point at $t = 2.8$ ms. Later, the point contact grows in both diametrical and longitudinal direction as seen in

time $t = 3.1$ ms. This contact front is seen to grow with an average velocity of 70 m/s, which is relatively small as compared to free-field implosion experiments contact velocity (between 150 – 200 m/s [10]). This slower velocity of contact front propagation and the deceleration of walls prior to wall contact is an effect of confined environment. An implosion process is typically a relatively longer event (of the order of ~ 1 ms) as compared to the diametrical reflection-free time period inside the confining tube (~ 60 μ s). Thus, the confining tube inhibits the free-propagation of low pressure implosion during wall recession. As a result, these waves reflect back from the inner walls of the confining tube and superimpose leading to large dynamic pressure drop. Therefore, the pressure in near-field of implodable volume is always smaller as compared to an equivalent free-field implosion experiment. With significant pressure drop, the driving forces to continue the implosion process decreases and hence the overall structural velocities are smaller as seen in Fig. 5.

The velocity contours for EXP-W43, shown in Fig. 5(b), are very similar as seen in free-field situations due to high collapse pressure of the geometry. The implodable volume accelerates till time $t = 0.81$ ms, reaching a peak velocity of 28 m/s. In comparison to EXP-W29, a smaller duration, ~ 0.2 ms, deceleration phase is seen, which causes velocity to drop to 24 m/s prior to contact initiation. The initiation of contact occurs at $t = 1.03$ ms at the center point. The average velocity of contact growth in EXP-W43 is ~ 95 m/s, which is 35% higher than EXP-W29.

For comparing the velocity between all the experiments, the velocity at the center point and two longitudinally offset points, M, M+ and M- respectively (as shown in Fig. 5) is calculated and plotted in Fig. 6. It is clear that the velocity at center

point M increases with increasing collapse pressure. Similar trend is seen for M+ and M- as shown in Fig. 6(b) and (c). It is also seen the time of peak velocity is smaller with increasing collapse pressure. As the inner diameter is approximately identical across all geometries, the wall contact initiation occurs quicker in the case of higher collapse pressure experiments.

Figure 6 Velocity histories for all the experiments (a) point M, (b) point M+ and (c) point M-

Figure 6 also shows that the time duration of declaration phase decreases with increasing collapse pressure. Thus it can be concluded that if the collapse pressure is increased, the structural deformation occurs at a faster rate and the effect of confining environment on the structural velocity reduces.

3.2.2 Pressure History Comparison

The process of evolution of these implosion waves is very similar for all the experiments and the pressure history observed in the experiments inside the confining tube is shown in Fig. 7. The incompressible nature of water causes a sudden drop in the pressure at the nearest sensor (Ch-4) with the progress of implosion process, and these low pressure waves travel in both axial directions (Ch-4 $\rightarrow$ Ch-1 (downwards) and Ch-4 $\rightarrow$ Ch-7 (upwards)). As the upward end is open to the hydrostatic pressure, these waves reflect as a free boundary reflection and generate high water velocity at the open end towards inside. It causes the in-rush of water from the open end to compensate for the low pressure present in the confining tube. On the other hand, the low pressure waves reflect as a fixed boundary reflection from bottom end-plate and pressure is greatly reduced. This reduction also can even lead to the cavitation at the bottom end-plate (see the value of pressure signal is lower than -1 indicating cavitation in Fig. 7(b)).

At the completion of the collapse of the implodable volume, the velocity of water at the open end is still in the downwards direction. This in-rushing water hits the bottom end-plate and the velocity of the water at the end-plate abruptly vanishes. Thus the following in-rushing water over-compresses the water in front and a high pressure water hammer type wave forms inside the confining tube at the closed end-plate. The water hammer waves have been extensively investigated due to potential of failure in water pipelines. In water pipelines, the water hammer occurs by a sudden closure of a valve in running water pipelines. In the aforementioned implosion experiments, the implosion process causes a net inwards water velocity due to a collapse occurring

inside the confining tube, which acts as running water pipeline. But the presence of closed end-plate at the bottom end restricts the velocity leading to generation of water hammer type waves. As the evolution of hammer wave occurs primarily at the closed end-plate, the intensity of hammer pressure is highest at the closed end similar to seen in [16]. The plots in Fig. 7 are normalized in y axis with the critical pressure for the relative comparison across the experiments. For experiments conducted in this study, the maximum pressure at the end-plate is seen to be between $1.35P_c$ and $1.92P_c$.

In order to correlate the features in pressure history with structural deformations, two following key events of the implosion are marked in Fig. 7: A $\rightarrow$ wall contact, and B $\rightarrow$ contact propagated to half longitudinal length. Occurrence of A primarily indicates that the implodable volume is collapsed about $\frac{1}{4}$ of the total internal volume. Occurrence of B is an indication of the $\frac{3}{4}$ collapse (or nearly complete collapse) of the implodable volume. It can be seen in all the experiments that the pressure history near the implodable volume is always smaller than P_c prior to B, while after B, the pressure is larger than P_c . This suggests that the time duration of under-pressure region observed near implodable volume during these confined implosions is approximately equal to the duration of collapse of the implodable volume. This observation can also be understood by interaction of implosion wave with the confining tube. During the collapse of implodable volume, the low pressure waves are constantly emitted from the surface of implodable volume. Hence, it doesn't allow the pressure near the specimen to rise beyond the hydrostatic pressure. Only after the completion of collapse, the high pressure open end allows high pressure

waves to enter the confining tube and the pressure near the implodable volume increases beyond P_c .

Figure 7 Pressure History at the end-plate sensor (Ch-1) and near the specimen (Ch-4). (a) Ch-1 pressure history for EXP-W29 and EXP-W35 (b) Ch-1 pressure history for EXP-W38 and EXP-W43, (c) Ch-4 pressure history for EXP-W29 and EXP-W35 (d) Ch-1 pressure history for EXP-W38 and EXP-W43. Note that pressure on y-axis is normalized with the collapse pressure. Arrow “A” indicates the initiation of wall contact at the center point in the implodable volume. Arrow “B” indicates the time at which at least half of the unsupported length of implodable volumes makes contact.

The plots in Fig. 7 are grouped in two sections, first section contains EXP-W29 and EXP-W35, and second section contains EXP-W38 and EXP-W43 for easy readability of time history. As seen from DIC measurements, higher values of P_c generate faster implosions, and so smaller collapse durations. Therefore the hammer wave evolves faster for higher P_c as shown in Fig. 7. The hammer wave evolves at ~ 2.1 ms for EXP-W43, while it evolves at ~ 4.1 ms (approximately two times that for EXP-W43) for EXP-W29.

3.2.3 Average Velocity and Average Pressure Trend

The peak velocity and the average velocity of the center point (P) prior to contact are estimated and are plotted against the collapse pressure of the experiments as shown in Fig. 8(a). Both the average velocity and the peak velocity have a linearly increasing trend with increasing P_c . Although the average hammer pressure observed at the end-plate during the water hammer wave impact is found to be increasing linearly with P_c (see Fig. 8(b)), the peak hammer pressure does not have a linearly increasing trend. It suggests that the peak hammer pressure occurs in a very short time event and therefore the temporal changes in velocity history significantly affect the peak hammer pressure value.

Figure 8 (a) Average and Peak Velocity for all the experiments (b) Average and Peak Hammer Pressure for all the experiments

4. Conclusions

An experimental investigation is conducted to understand the evolution of water hammer type waves resulting from an implosion occurring inside a confining tube. Both pressure measurements along with high speed 3-D DIC measurements are

conducted to correlate the structural deformation with pressure history. The key findings of this study are as follows:

- (1) The presence of open ended confining tube around an implodable volume significantly affects the implosion process. The velocity during collapse is highly reduced due to the large pressure drop from the superposition of low pressure implosion waves inside the confining tube. The peak velocity and the average velocity prior to wall contact are found to be linearly varying with the collapse pressure.
- (2) The confining environment of open ended confining tube leads causes the implodable volume to decelerate well before the initiation of wall contact. This duration is also found to increase with decreasing collapse pressure and it reaches a significant duration of 1.5 ms for the lowest collapse pressure experiments conducted in this study.
- (3) The peak strength of water hammer wave in this study is found to be in between $1.35P_c$ and $1.92P_c$. The average strength of water hammer wave is $0.7P_c$.
- (4) The calibration using a submerged calibration grid can successfully account for the refractive index mismatch between the water/cylindrical acrylic window/flat acrylic window/air. The calibration experiments reveal that the both the in-plane and out-of-plane measurements can be measured using this modified 3-D DIC calibration procedure within 5% error.

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CHAPTER 4

SYMPATHETIC UNDERWATER IMPLOSION IN A CONFINING ENVIRONMENT

by

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Abstract

An experimental study is conducted to investigate the phenomenon of sympathetic underwater implosion of cylindrical metallic shells in a confining environment. Two aluminum 6061-T6 implodable volumes with different collapse pressures are placed inside a confining tube with one end open to the environment and are hydrostatically loaded upto the weaker implodable volumes' critical collapse pressure. Experiments show that implosion of the weaker implodable volume (critical pressure = P_c) inside the confining tube leads to the subsequent sympathetic implosion of the stronger implodable volume (critical pressure = $1.2P_c$). Implosion of the weaker implodable volume produces strong oscillating water hammer waves with $1.6P_c$ peak over-pressure, which initiates the implosion of the stronger implodable volume. Pressure histories recorded within the confining tube indicate that the sympathetic implosion of the stronger implodable volume generates low pressure high frequency implosion waves. The superposition of the low pressure waves with the high pressure water hammer waves mitigate to a great extent the later cyclic water hammer loading within the confining tube.

Keywords

Sympathetic Implosion, Implodable Volume, Collapse, Pressure Waves, Fluid Structure Interaction, Water Hammer, Confining Environment

1. Introduction

Thin walled structures are extensively used in underwater applications such as submarines, underwater remote operated vehicles, and underwater pipelines etc [1–3]. The presence of lower pressure, uncompensated gas inside the structure and the large external hydrostatic pressures lead to a net pressure differential across the walls of the structures. If the hydrostatic pressure exceeds a certain critical limit for a given structure [4,5], it becomes unstable and collapses/implodes onto itself. This implosion process is shown to be highly violent in nature with resulting high velocity water motion and strong shock waves [6]. There have been several investigations reported by researchers in naval and marine communities on the mechanics and fluid-structure interaction during an implosion process [2,7–19]. Although the evolution of implosion pressure waves and its relation to structure geometry has been widely studied, there is a little information on the interaction of these pressure waves with the adjacent structures and on the potential of damage/sympathetic implosion. Orr and Schoenberg conducted experiments with a pre-weakened glass sphere and a non-weakened glass sphere submerged together for the possibility of sympathetic implosion [12], although none was observed. Later, Harben and Boro bundled five glass spheres together and mechanically initiated one of them at a prescribed depth [20]. It was reported that sympathetic implosion did occur in these experiments. The accidental failure of 7000 photomultiplier tubes at the Super Kamiokande laboratory in a chain reaction is a classic example of sympathetic implosion of near-field structures [21].

The damage potential of an implosion pressure pulse is generally estimated by the pressure-impulse and the energy flux released during the implosion event. Since

both the pressure-impulse and the energy flux released in an underwater free-field implosion decays in a spherical manner [2] (pressure-impulse as $1/r$ and energy flux as $1/r^2$), a structure located at a sufficient distance from an imploding structure can be considered safe from the design point of view. But in certain situations, there may exist a confining structure around the implodable volume. In the event of an implosion occurring in such situations, the confining environment significantly alters the implosion process due to strong fluid-structure interaction with the confining environment [14,19,22]. Hence, all the energy released during the implosion process focuses inside the confining structure. This phenomenon leads to generation of extremely strong water hammer waves with significant time period [22]. Therefore, implosion of the weakest implodable volume inside the confining tube may damage the adjacent stronger structures. In present study, an experimental investigation is conducted to understand the damage to the adjacent structures placed inside the confining tube with a weaker implodable volume. Study reveals that the implosion of an implodable volume inside the confining tube can generate a large enough pressure pulse to initiate implosion of comparatively stronger structures.

2. Experimental setup

The implosion experiments are conducted inside an underwater pressure vessel facility at the University of Rhode Island. The implodable volumes chosen in this study are made out of commercially available aluminum 6061-T6 seamless extruded tubing with an outer diameter ($2a$) of 38.1 mm (1.50 in) and 0.870 mm (0.0343 in) nominal wall thickness (h). All the primary and secondary implodable volumes are

cut out from single 1.8 m long (72 in) extruded tubing, thus there exists “no change in average wall thickness” between the implodable volumes² and implodable volumes with nearly identical and repeatable buckling pressures are manufactured as primary implodable volumes. Table 1 provides a summary of the experiments conducted and the details of the specimens used in this study. Ovality parameter

$(\Delta_0 = (a_{\max} - a_{\min}) / (a_{\max} + a_{\min}))$ and wall eccentricity parameter

$(\Xi_0 = (h_{\max} - h_{\min}) / (h_{\max} + h_{\min}))$ are measured for each specimen to quantify the initial imperfections present in the specimen prior to experiments [23] and are also shown in Table 1. Aluminum end-caps equipped with o-ring seals are press fitted at both the ends to avoid leakage of water into the implodable volume [14,15,19]. The theoretical buckling pressure (P_c) of the implodable volumes can be calculated from the

following equation (derived by von-Mises [4]):

$$P_c = \frac{Eh}{a} \frac{1}{n^2 + \frac{1}{2} \left(\frac{\pi a}{l} \right)^2 - 1} \left\{ \frac{1}{\left[n^2 (l/\pi a)^2 + 1 \right]^2} + \frac{h^2}{12a^2(1-\nu^2)} \left[\left(n^2 + \left(\frac{\pi a}{l} \right)^2 \right)^2 - 2n^2 + 1 \right] \right\} \quad (1)$$

where the parameters are listed in Table 2. The unsupported length of the primary (weaker) implodable volume is 302 mm, while the unsupported length for the secondary (stronger) implodable volume is $\sim 1/2$ of the primary implodable volume

² Due to the extruded nature of the tube, the geometry along with imperfections is consistent in direction of extrusion (i.e. length direction). Thus the average wall thickness is identical for single extruded tubing at any longitudinal location. This fact can be observed in the author’s previous article [19], which shows three experiments of specimens cut from single extruded tubing. These experiments showed collapse pressure to be within 0.02 MPa (2 psi) for three different experiments.

(163 mm), hence the theoretical buckling pressure of secondary implodable volume is 20% higher than primary implodable volume.

Expt. Name	Tube	Length (l)	Expt. Collapse Pressure (P_c)	Ovality (Δ_0)	Wall Eccentricity (Ξ_0)	Average Wall Thickness from Weight (h_2)
EXP-P	Primary Implodable Volume – I	302 mm	3.59 MPa (520 psi)	0.01%	2.53%	0.870 mm (0.0343 in)
EXP-PS	Primary Implodable Volume – II	302 mm	3.54 MPa (513 psi)	0.05%	2.34%	0.867 mm (0.0341 in)
	Secondary Implodable Volume	163 mm	4.24 MPa (615 psi) ³	0.02%	1.78%	0.871 mm (0.0343 in)

Table 1 Details of the experiments conducted. The average wall thickness from weight are calculated by estimating the weight of the implodable volume using volume of the material multiplied by the density of aluminum (2700kg/m³)

Parameter	Value
E (elastic modulus)	70 GPa
ν (Poisson's ratio)	0.3
a (radius of middle surface of implodable volume)	15.44 mm
h (wall thickness)	~ 0.870 mm
l (unsupported length)	302 mm (primary) 163 mm (secondary)
n (circumferential mode number)	2

Table 2 Properties of implodable volume material and its dimensions

³ This is not the experimentally calculated value. It indicates an estimated value predicted by Eq. (1) by changing length from 302 mm to 163 mm. Primary EXP-PS is used as the reference experimental collapse pressure for this estimation.

The implodable volumes are placed concentrically inside a confining tube with one open end within the pressure vessel facility. The confining tube sits inside a 2.13 m diameter (84 in) underwater pressure vessel facility. (See [15] for detailed description of facility). A schematic of the confining tube with the implodable volumes is shown in Fig. 1. The confining tube has an inner diameter (D) of 178 mm (7.00 in) with 25.4 mm (1.00 in) wall thickness and is made out of aluminum 6061-T6. The total length (L) of the confining tube is 1.27 m (50.0 in). The bottom end of the confining tube is closed with an end-plate and the top end is open to the high pressure water environment of the underwater pressure vessel. High frequency dynamic pressure sensors (PCB-113B22) are installed in the inner wall of the confining tube at various locations. The signal from the pressure sensors is recorded using a 200 KHz bandwidth recorder at a sampling rate of 2 MHz.

To understand the evolution of water hammer pressure waves due to the primary implodable volume, an implosion experiment is first conducted with only the primary implodable volume. In this experiment (EXP-P in Table 1), the primary implodable volume is placed near the closed end of the confining tube (at Ch-3) as shown in Fig. 1(b) and the implosion occurs at 3.59 MPa (520 psi). In the second experiment (EXP-PS), the secondary implodable volume is placed just above the primary implodable volume (between Ch-4 and Ch-5) as shown in Fig. 1(c). In this experiment, the implosion of primary implodable volume occurs at 3.54 MPa (513 psi).

Figure 1 Schematic of the open-ended confining tube implosion facility. (a) Underwater pressure vessel facility at University of Rhode Island. The confining tube is placed at the center of pressure vessel. (b) A detailed schematic of open-ended confining tube indicating the exact sensor locations for EXP-P. The primary implodable volume is located such that the axial center is in front of Ch-3 pressure sensor. (c) Schematic for a sympathetic implosion experiment (EXP-PS). The secondary implodable volume is placed above the primary implodable volume.

3. Experimental results and discussion

The pressure history observed in the confining tube for the experiment containing only the primary implodable volume (EXP-P) is shown in Fig. 2(a). In order to describe the overall behavior of pressure evolution throughout space, a time evolution of pressure is also shown in Fig. 2(a) indicating the various phases of primary implodable volume's implosion in the confining tube. The incompressible nature of water causes a sudden drop in the pressure at the nearest sensor (Ch-3) with the progress of implosion process, and these low pressure waves travel in both axial directions (Ch-3 → Ch-1 (downwards) and Ch-3 → Ch-7 (upwards)). These low

pressure waves are indicated by P1 in Fig. 2(a). As the upward end is open to hydrostatic pressure, this wave reflects as a free boundary reflection and a reflected high pressure wave enters inside the confining tube (indicated by wave P2 in Fig. 2(a)). This generates high water velocity at the open end towards inside and compensates for the low pressure present in the confining tube. On the other hand, the low pressure waves reflect as a fixed boundary reflection from bottom end-plate and pressure is greatly reduced upto the cavitation pressure (see Ch-2 at ~ 1.0 ms). After the pressure reaches zero near the end-plate, a cavitation bubble is formed due to separation of water from the end-plate. This bubble grows to its largest size and collapses onto itself generating a small local pressure pulse seen between 1.5 ms - 2.0 ms at Ch-2 [24].

When the high pressure wave P2 reaches the implodable volume, the ongoing collapse causes additional low pressure waves to release seen as pressure wave P3. At the completion of the collapse of the implodable volume, the water velocity at the open end is still in the downwards direction. This in-rushing water hits the end-plate and the velocity of the water at the end-plate abruptly vanishes. Thus the following in-rushing water over-compresses the water in front and a high pressure water hammer type wave forms inside the confining tube at the closed end-plate seen as P4 in Fig. 2(a). This can be seen by 5.0 MPa peak over-pressure achieved at ~ 3.1 ms in Fig. 2(a). The gradual reduction of water velocity throughout the tube generates high pressure wave at the other locations in the upper confining tube. These high pressure water hammer waves are oscillatory in nature. The confining tube is cyclically loaded with high pressure at a time period of ~ 3.7 ms (at ~ 270 Hz). It is important to note

that due to their large time duration period, these waves accumulate large impulse during high pressure cycle, which can potentially implode other structures in the near-field.

Figure 2 (a) Left figure plots the evolution of dynamic pressure in the entire length of the confining tube for experiment EXP-P. The y-axis is the axial direction with zero being the closed end-plate and 1.27 m represents the open end of the confining tube. The color intensity indicates the pressure contour level. The right figure is the dynamic pressure history plot for three near-field pressure sensors, Ch-2, Ch-3 and Ch-4, for EXP-P experiment. The hashed region indicates the approximate over-pressure pressure pulse experienced by the secondary implodable volume during the EXP-PS experiment. The bottom line in the hashed region represents the critical buckling pressure (4.24 MPa) of the secondary implodable volume. (b) Left figure plots the evolution of dynamic pressure for experiment EXP-PS. The right figure plots the dynamic pressure history inside the confining tube for EXP-PS experiment. Ch-2 and Ch-4 are clearly seen oscillating out-of-phase from each other with Ch-3 being the baseline.

The pressure history and time evolution of pressure observed for the experiment (EXP-PS) of primary implodable volume placed with a secondary implodable volume is shown in Fig. 2(b). Results show that the initial collapse of the primary implodable volume is unaffected by the presence of secondary volume and

the description of P1 to P4 waves is similar as described earlier in this section. The low pressure phase until 2.5 ms is identical for both experiments (see pressure time evolution in both Fig. 2(a) and (b)).

After the generation of high pressure water hammer wave (P4) in the confining tube, the secondary implodable volume experiences much higher transient pressure loading than its critical buckling pressure, 4.24 MPa (615 psi). Pursuant to the arrival of first hammer wave (see Fig. 2(a)), starting from $t = 2.7$ ms, the peak pressure experienced by the secondary implodable volume is ~ 4.55 MPa higher than its own buckling pressure. *This is the primary cause of initiation of implosion of the secondary implodable volume.* This phenomenon is similar to a dynamic buckling problem (i.e. a structure subjected to a transient pressure load) earlier reported by Goodier and McIvor [25] and, Lindberg and Florence [26]. In such dynamic situations, the peak pressure along with the impulse of the pressure wave determines the onset of instability/buckling for a given implodable volume (or the adjacent structure) [25–27]. In this study, the high pressure water hammer wave acts as the impulsive loading and it causes the secondary volume to start collapsing at $\sim t = 3.7$ ms indicated by a sharp decrease in pressure at the nearest sensor Ch-4. These low pressure waves (denoted by S1U- and S1D-) travel in both upwards and downwards direction exhibiting consecutive sharp pressure drop at every sensor location. The implosion event of secondary implodable volume is relatively faster than primary implodable and is indicated by the very sharp pressure drop (see strong blue region towards top side at ~ 4.25 ms following S1U-). The superposition of initial water hammer wave (P4) present in the lower confining tube with S1D- results into slowly decaying S1D-. The

reflection from open-end of S1U- and its interaction with itself generates a high pressure wave indicated by S2+, while the reflection from the closed end-plate results into low pressure reflected wave indicated by S2-. Later, two upward travelling high pressure waves originate near the closed end-plate marked by ① and ②. The repeated reflection of these waves from the open-end of the confining tube and from the closed end-plate, along with their interference with each other results into high frequency pressure oscillations inside the confining tube.

Interestingly, as the onset of implosion occurs at the instant of highest pressure, these low pressure implosion waves of the secondary implodable volume superimpose over the high pressure hammer wave of primary implodable volume as a partially destructive interference (see Fig. 2(b)). It implies that the secondary implodable volume can effectively absorb the energy present in the first high pressure hammer wave, which is beneficial in reducing the repeated cyclic loading of the water hammer wave as seen in the case of primary implodable volume⁴.

An inspection of the pressure time history shows differences in the frequency components of the pressure waves between the two experiments as seen in Fig. 3(a) and (b). From the water hammer theory [28,29], the frequency of the water hammer wave can be written as $f_1 = c/4L = 264\text{Hz}$ (where c = coupled longitudinal pressure

⁴ Note that, although the experiments conducted in this study show a destructive interference of waves exhibiting mitigation of water hammer wave, it may not be necessarily true for all different variations in the configuration (such as varying collapse pressures between the two implodable volumes or their relative placement inside confining tube). Hence, a carefully chosen design would be needed to use sympathetic implosion as a mitigation tool. For example, an implodable volume equipped with an initiating mechanism at prescribed critical dynamic pressure can be used as an active mitigation device.

wave speed in the confining tube (1341 m/s) [14,30,31]), therefore the peak at a frequency of 256 Hz in Fig. 3(a) indicates the cyclic water hammer wave due to primary implodable volume implosion (EXP-P). In the case of sympathetic implosion (EXP-PS), the collapse of the secondary implodable volume results into a high frequency oscillated wave travelling over a low frequency wave (see time history in Fig. 2(b) beyond 5.5 ms). A sequential evolution of these high frequency waves is also shown in Fig. 2(b). If we trace two high frequency waves (shown by no. 1 (solid line) and 2 (dashed line) in Fig. 2(b)), it is seen that these waves oscillate in the entire length of the confining tube. Thus the frequency of the oscillation can be approximately written as $f_2 = c/L = 1055 \text{ Hz}$. Fourier transform of the sympathetic implosion pressure history signal shown in Fig 3(b) shows a similar frequency peak (at $\sim 1200 \text{ Hz}$), which corresponds to high frequency oscillations.

Figure 3 (a) Frequency spectrum plot for experiment EXP-P. The inset figure shows the zoomed spectrum plot between 200 Hz and 300 Hz. (b) Frequency spectrum plot for experiment EXP-PS. The inset figure shows the zoomed spectrum plot between 1150 Hz and 1250 Hz.

Although sympathetic implosion of a stronger implodable indicates the detrimental nature of water hammer wave, it can be desirable in certain situations to avoid the high magnitude cyclic hammer loading of a single implosion. In this way, a stronger implodable volume can act as a sacrificial implodable to mitigate the water

hammer wave. In order to compare the effectiveness of such concept, the damage potential of the pressure wave generated between the two experiments is estimated by comparing the following parameters: (I) mean dynamic pressure inside the confining tube, (II) the pressure-impulse associated with the positive pressure cycle and (III) the total compressible potential energy present in the confining tube. Fig. 4(a) shows that the sympathetic implosion decreases the magnitude of mean dynamic pressure oscillations from 3.04 MPa to 1.72 MPa. Thus, a 43% reduction in the magnitude of cyclic pressure loading dramatically reduces the fatigue damage. The positive pressure impulse (shown in Fig. 4(b)) present in the water hammer pressure wave also decreases with the initiation of the sympathetic implosion. It causes the maximum pressure-impulse to decrease by 30% in the first water hammer cycle and the pressure-impulse present in the following cycles also decreases significantly.

The compressible potential energy ($E(t)$) present inside the confining tube can be written as,

$$E(t) = \int_0^L \frac{\pi D^2}{4} \frac{P(x,t)^2}{2K} dx \quad (2)$$

where K = bulk modulus of water, $P(x,t)$ = dynamic pressure as a function of axial location (x) and time (t). The comparison of compressible potential energy shown in Fig. 4(c) suggests that sympathetic implosion absorbs upto 70% potential energy present in the 2nd and 3rd water hammer cycles. The confining tube contains ~ 7 J of energy in the case of sympathetic implosion as compared to 22.5 J for the primary implodable implosion.

Figure 4 (a) Plot of mean dynamic pressure in the confining tube as function of time. The mean dynamic pressure is calculated by spatial averaging of the dynamic pressure. (b) Plot of positive pressure-impulses present in the each hammer wave loading. Initial three hammer wave cycles are shown in Fig. 2(a). (c) Plot of total compressible potential energy present in the water inside the confining tube as a function of time.

4. Conclusions

The problem of sympathetic implosion in the presence of a confining environment is experimentally investigated. The study shows that the implosion of a structure inside a confining environment leads to strong oscillating water hammer waves which can be potentially dangerous to the other near-field structures. To demonstrate such phenomenon, a 20% higher critical pressure structure is shown to be sympathetically imploded due to the water hammer waves generated by an implosion event. Thus the presence of a weaker structure inside a confining environment is a significant risk to other stronger structures. The peak dynamic pressure of the water hammer wave is found to be approximately 1.6 times the critical collapse pressure in these experiments. Results also indicate that the sympathetic implosion event superimposes to primary implosion's water hammer wave in a destructive manner in this study and it absorbs/mitigates most of the impulse and released energy. Hence, further cyclic pressure loading is highly reduced due to the sympathetic implosion.

Thus, a carefully designed sacrificial implodable volume in the event of an implosion can act as an effective tool for hammer wave mitigation.

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CHAPTER 5

MECHANICS OF THE IMPLOSION OF CYLINDRICAL SHELLS IN A CONFINING TUBE

by

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Abstract

A fundamental experimental investigation, with corresponding computational simulations, was conducted to understand the physical mechanisms of implosions of cylindrical shells occurring within a tubular confining space which has a limited potential energy reservoir. In particular, attention was focused on studying the generation of pressure waves from the implosion, the interaction of the pressure waves with the confining tube walls and end caps, and the collapse mechanisms of the implodable volume. Experiments were conducted with three implodable volume geometries which had similar critical collapse pressures. The implodable volumes were aluminum 6061-T6 cylindrical tubing and were placed concentrically within the confining tube. Pressure histories recorded along the length of the confining tube during the experiments were utilized to analytically evaluate the deformation of the implodable volume using fluid-structure coupled deformation models. Computational simulations were conducted using a coupled Eulerian-Lagrangian scheme to explicitly model the implosion process of the tubes along with the resulting compressible fluid flow. The numerical model developed in this study is shown to have high correlation with the experimental results and will serve as a predictive tool for the simulation of the implosion of different cylindrical geometries as well as various tube-in-tube implosion configurations. The experimental results show that the limited hydrostatic potential energy available in a confined environment, as compared to a free field, significantly influences the implosion process. The wall velocities of the implodable volume during the collapse, as well as the extent of the collapse progression, are largely affected by the sudden decrease in the available hydrostatic potential energy.

This energy is shown to be partially transformed into elasto-plastic strain energy absorbed in the deformation of the implodable volume, as well as the kinetic energy of the water during the implosion process. Experiments also show that the extent of the collapse progression of an implodable volume can potentially be inhibited within a closed environment, which can lead to the arresting of an implosion event prior to completion for larger implodable volumes. The pressure waves generated during collapse comprise of waves emitted due to the impact of the implodable volume walls, the arrest of rushing water and contact propagation along the walls. These processes later evolve into water hammer type axial wave behavior.

Keywords

Implosion, Implodable Volume, Collapse, Pressure Waves, Computational Modeling, Fluid Structure Interaction, Water Hammer, Confined Environment

1. Introduction

A comprehensive series of experiments were conducted to study the mechanics of the implosion of cylindrical shells (implodable volume) in a tubular confining space. The emphasis was on understanding the physical mechanisms of implosion of cylindrical shells occurring within a confining space with limited potential energy reservoir. The implodable volumes consisted of aluminum 6061-T6 cylindrical tubing, and were placed concentrically and longitudinally centered within the confining tube. The collapse pressure was held approximately constant throughout the study. The

pressure histories generated by the implosion event were captured by dynamic pressure transducers mounted on the inner surface of the confining tube.

Understanding the fundamental mechanisms associated with the implosion process has been a topic of interest since the early 1950's, especially in the marine pipelines and naval communities (Isaacs and Maxwell, 1952; Palmer and Martin, 1975; Turner, 2007; Urick, 1963). Typical examples of implodable volumes include deep ocean submersibles, submarines, underwater remote operated vehicles, underwater pipelines, and underwater sensors (Turner and Ambrico, 2012). An implodable volume can be defined as any structural shell or body that is acted upon by external pressure and contains internal gas at a lower pressure (or vacuum). In simple terms, the implosion of a structure can be understood as a sudden loss of stability due to a net external force causing the structure to collapse onto itself. The resulting collapse of the structure is violent and results in rapid release of energy in the form of shock pressure waves, high velocity fluid motion, and sound ("Blast Mitigation - Experimental and Numerical Studies, Chapter 6," 2014). An axial/lateral loading, uniform hydrostatic pressure or a combination of both can be the initiating and driving forces for the implosion of a structure. In underwater environments, this collapse is a dynamic process with duration of the order of milliseconds. At the onset of collapse, the implodable volume walls gain inwards momentum and the surrounding water rushes in to fill the resulting void generated in the collapse process. The fast inward travelling water surrounding the receding walls of a collapsing structure stops suddenly when the walls come into contact. However, the acquired momentum of the in-rushing water causes it to over-compress against the structure and produces strong

outwardly radiating shockwaves. Such pressure pulses/shock waves can be large enough to potentially damage or even lead to the implosion of adjacent structures (Diwan et al., 2012; Farhat et al., 2013; Harben and Boro, 2001; Ikeda, 2012; Ling et al., 2013; Orr and Schoenberg, 1976; Turner and Ambrico, 2012; Vath and Colletti, 1968). A classical example of such an event is the 2001 accident at the Super-Kamiokande facility in Japan, where about 7000 photomultiplier tubes were destroyed by a sympathetic implosion event (Cartlidge, 2001).

In the past, many researchers have theoretically investigated the buckling of cylindrical shells to predict the critical hydrostatic collapse pressures (Timoshenko and Gere, 1963; von Mises, 1929). The effect of imperfections and various defects has been presented in several research articles (Budiansky and Hutchinson, 1966; Simitses, 1986). It was concluded that the initial ovality of the cylindrical shell can significantly reduce the collapse pressure, while the variation in wall thickness has a minimal effect on the collapse pressure (Kyriakides and Corona, 2007). The propagation of buckles in offshore pipelines has also been widely studied (Charter et al., 1983; Kyriakides and Babcock, 1981; Kyriakides and Netto, 2000; Mesloh et al., 1973; Palmer and Martin, 1975).

Although the problem of buckling has been extensively investigated from the structural point of view, there have been very few studies reported which aim to understand the fluid motion and pressure wave emissions during underwater buckling of structures. In the early 1900's, Rayleigh (Rayleigh, 1917) developed analytical expression for the collapse of a spherical bubble inside an incompressible fluid. According to this theory, a pressure difference between the bubble and ambient

internal pressure causes the bubble to collapse or grow, which results in an oscillatory bubble pulse. In the problem of a closed structure imploding in an underwater environment, the low pressure gas is contained inside a structure. Due to this fact, this structure plays a key role during the collapse process and the complex fluid-structure interaction between the structure and water governs the dynamics of the implosion. In the early 1960's, the implosion of glass spheres was used to generate acoustic signals for underwater applications (Isaacs and Maxwell, 1952; Urick, 1963). Orr and Schoenberg conducted implosion experiments using pre-weakened glass spheres (by grinding a flat spot) in the ocean and concluded that the implosion depth is dependent on the thickness of the flat spot (Orr and Schoenberg, 1976). Harben and Boro conducted implosion experiments with five glass spheres bundled together for boosting the amount of implosion energy released (Harben and Boro, 2001). Turner conducted near-field pressure measurements during the implosion of glass spheres and concluded that the failure time history of the structure has a significant influence on an implosion pressure pulse. Recently, Turner and Ambrico (Turner and Ambrico, 2012), and Farhat et al. (Farhat et al., 2013) studied the implosion of aluminum cylindrical tubes. Turner and Ambrico concluded that there are four primary features of the implosion process in metal tubes: (1) the initial collapse phase, prior to wall contact, is accompanied by a smooth decrease in pressure in the surrounding water, (2) at the moment that contact is made between opposing sides of the collapsing cylinder at the center, a short duration pressure spike is emitted in the surrounding water, (3) a large positive pressure is produced at the instant that contact between the two opposing sides extends the full width of the cylinder, and (4) as the buckle propagates toward

the ends, the pressure pulse continues, but at a lower magnitude, until the buckle reaches the end cap and the collapse of the cylinder completes. Farhat et al. (Farhat et al., 2013) extended this implosion work by studying both mode-2 and mode-4 collapse of aluminum cylindrical shells and demonstrated that the pressure pulse generated is influenced by the mode of buckling as well as the associated localization of collapse.

With advances in computational computing resources and efficiency, the modeling and simulation of implosion phenomenon has gained interest. Early numerical studies involving underwater implosions generally represented the implodable volume as a gas bubble within a high pressure fluid field. Kadioglu and Sussman (Kadioglu and Sussman, 2008) utilized an adaptive solution methodology to solve the multi-phase problem of a gas bubble contained within a water field. Farhat et al. (Farhat et al., 2008) simulated the 2 phase flow problem with a solution methodology known as the “ghost fluid method for the poor” (GFMP). The work extended the original GFMP method to better handle the large discontinuities of pressure and density at the air/water interfaces. These studies have generally focused on the two-phase nature of the fluid flow but have neglected the fluid structure interaction which has been shown to be important in the implosion of a structural body. Turner (Turner, 2007) utilized the coupled Eulerian-Lagrangian fluid structure interaction code (Dynamic System Mechanics Analysis Simulation code or DYSMAS) to simulate the collapse of glass spheres to determine the influence of the failure rate of the spheres on the resulting pressure histories. The primary conclusion was that a computational model of an underwater implosion event must include the structure that separates the low pressure air from the high pressure water. If the structure is

neglected, the model would over predict the peak pressure generated from the collapse. Turner and Ambrico (Turner and Ambrico, 2012) also utilized the DYSMAS code to simulate the implosion of metallic cylindrical bodies. Farhat et al. (Farhat et al., 2013) investigated the pressure pulses resulting from an implosion, and the parameters which influence the nature of the pulses, through the AERO software suite. The simulations were shown to accurately capture both the deformations of the structure as well as the pressure waves resulting from the collapse. Recently, Chamberlin et al. (Chamberlin et al., n.d.) developed energy metrics to characterize underwater implosion and used these metrics to examine the energy balance during an implosion event. For a series of test cases involving hydrostatic implosion of ductile metal cylinders, they found that the structure absorbs the majority of the initial energy of the system and the released pressure pulse carries away a small percentage of the initial energy (Chamberlin et al., n.d.).

All these experimental and numerical studies on implosion mentioned above were conducted in a free-field environment, where the net hydrostatic pressure in the surrounding fluid is maintained during the implosion process and the free-field environment acts as an infinite source of hydrostatic potential energy to drive the implosion process. On contrary, in the case of implosion occurring within a confining space, the source of hydrostatic potential energy to progress the implosion is limited. Hence, the ratio of energy required to deform the implodable volume and the initial potential energy available acts as a critical factor to drive the implosion process. Costa and Turner (Costa and Turner, 2008) did study the implosion of a tube occurring within an open-ended confining tube and showed that the implosion phenomenon

significantly differs from a free-field implosion, however, due to open ended nature of their confining tube, infinite energy was still available for driving the implosion process. Such implosions have shown to be capable of generating strong hammer pressure waves (up to 5 times of the collapse pressure) at the closed end of the confining tube, which is highly detrimental to the nearby structures. Another study on understanding the implosion of structures in a confining space was performed by Bitter and Shepherd (Bitter and Shepherd, 2014). They studied the dynamic buckling of submerged tubular structures in a closed tube. Tube buckling experiments were performed by applying impulsive loading in the water filled outer tube and dynamic buckling theoretical models were developed involving the fluid structure interaction. In their experiments, the dynamic impulse loading applied was an order of magnitude higher than the critical buckling load, therefore the dynamic impulsive loading was only considered to cause the implosion and the effect of initial hydrostatic pressure was not investigated.

The primary objective of this study is to understand the mechanics of implosion of cylindrical shells inside a closed confining tube. The experiments conducted with a range of d/D (diameter of implodable/diameter of the confining tube) cylindrical shells revealed that the limited hydrostatic energy available in the confined environment significantly influences the implosion process. The extent of collapse and the velocity of the implodable walls are smaller for large diameter cylindrical shells, although the change in internal volume of the implodable is relatively similar for all shells.

2. Experimental Setup

A schematic of the experimental setup for the tubular confined space implosion facility is shown in Fig. 1. It consists of a confining tube mounted on a horizontal I-beam section and an inner tube (implodable, shown in Fig. 2). The confining tube is made out of seamless low-carbon steel (SA106-B) pipe with the specifications from pressure vessel handbook (Buthod, 1983) and has an inner diameter of 177.8 mm (7 in) with 19 mm (0.75 in) wall thickness. The overall length of the confining tube is 2.13 m (84 in). The high frequency dynamic pressure transducers (PCB-113B22) are installed in the wall of the vessel such that the sensor element is flush with the inner surface. The dynamic pressure transducers have a rise time of $<1 \mu\text{s}$, resolution of 0.14 KPa (0.02 psi) and high resonant frequency ($>500 \text{ KHz}$). The transducers are placed along a single line axially in the confining tube, and the distance between the sensors varies between 76 mm (3 in) to 305 mm (12 in) depending on their axial position (see Fig. 1 for exact locations). Three sensors are placed in the close vicinity of the center of the confining tube to exclusively capture the fluid pressure drop with the collapse progression in the implodable. A data acquisition system with 200 KHz bandwidth is used to capture the dynamic pressure history in the confining tube at a sampling rate of 2 MHz. Additionally, a static sensor is installed to monitor the hydrostatic gage pressure of the confining tube before the implosion occurs. The confining tube is inclined at an angle of $\sim 2^\circ$ at one end manually in order to remove the excess air present in the confining tube to the maximum extent possible before pressurization.

Figure 1 Schematic of experimental setup

The implodables used in this study are made out of commercially available aluminum 6061-T6 extruded seamless tubing. Table 1 gives a brief layout of the experiments conducted in this study. The diameter of the implodable volume is increased from 31.8 mm (1.25 in) to 50.8 mm (2 in) to 76.2 mm (3 in) to increase the ratio of implodable diameter (d) to the confining tube diameter (D). The unsupported length of the implodables is fixed at 304.8 mm (12 in) and the wall thickness (t) is increased to keep the critical buckling pressure approximately constant. The increasing implodable diameter allows for a large range of the ratio between the confining space's volume and implodable's volume (from ~ 30 to 200). A schematic of the mounting fixture for holding the implodable is shown in Fig. 2. The implodables are sealed on

both ends using solid aluminum end-caps, which utilize circumferential o-rings for sealing the specimen for underwater experiments. This tube and end-cap assembly is mounted inside the confining tube using support collets, which ensure concentric placement of the implodable in the confining tube. The construction of the three spoke collet is chosen so as to provide minimal interference to the axial pressure waves and water flow within the confining tube during the implosion event. The effect of initial ovality and wall thickness variation on the critical buckling pressure has been widely reported (Kyriakides and Corona, 2007). Therefore, ovality parameter

$(\Delta_0 = (D_{\max} - D_{\min}) / (D_{\max} + D_{\min}))$ and wall eccentricity parameter

$(\Xi_0 = (t_{\max} - t_{\min}) / (t_{\max} + t_{\min}))$ are measured for each specimen to quantify the initial imperfections present in the specimen before experiments and are shown in Table 2.

Geometry No.	Wall Thickness (t)	Outer Diameter (d)	Internal Volume (V_1)	$V_1/V_{\text{confining tube}}$	l/d	d/t	Exp. Collapse Pressure
1	0.89 mm (0.035 in)	31.8 mm (1.25 in)	241 ml	1/187.5	9.6	35.7	4.14 MPa (600 psi)
2	1.24 mm (0.049 in)	50.8 mm (2.00 in)	616 ml	1/73.05	6	40.8	3.33 MPa (484 psi)
3	1.65 mm (0.065 in)	76.2 mm (3.00 in)	1386 ml	1/32.47	4	46.2	3.59 MPa (520 psi)

Table 1 Layout of the experiments

Geometry No.	Ovality Parameter (Δ_0)	Wall Eccentricity (Ξ_0)
1	0.04%	1.41%
2	0.06%	0.97%
3	0.07%	2.21%

Table 2 Ovality parameter and wall eccentricity for all geometries

Figure 2 Schematic of implodable volume

The confining tube is filled and pressurized with water using a hydrostatic pump. The pressurization rate during the experiments is kept at 0.689 MPa/min (100 psi/min). This ensures a negligible increase of pressure ($\sim 10^{-5}$ MPa) during the typical implosion event (< 2 ms). When the implodable reaches the critical buckling pressure, the implosion occurs and generates an audible noise. This noise is utilized as a signal to manually shut down the hydrostatic pump. Due to the relatively small flow rate of the hydrostatic pump (0.0935 ml/ms), the external water flow during the implosion event from outside in the confining tube can be neglected in the transient pressure time histories.

3. Computational Model

Computational models of the implosion experiments are developed using the Dynamic System Mechanics Analysis Simulation (DYSMAS) software, maintained by the Naval Surface Warfare Center (NSWC), Indian Head Division. The software is a fluid-structure interaction (FSI) code that consists of structural solver (ParaDyn), a fluid solver (GEMINI), and the standard coupler interface. ParaDyn is an explicit Lagrangian solver suitable for large deformation dynamic problems. GEMINI is an

explicit Eulerian fluid solver with equation of state (EOS) models for gases, liquids, explosives, and solids. The standard coupler interface allows the fluid and structural codes to share the required variables for the fluid structure interaction problem.

The computational models are developed to (1) capture the fluid structure interaction between the implodable and the surrounding fluid, (2) predict the pressure time history resulting from the implosion and, (3) predict the final collapsed shape of the implodable. The models include the implodable, the air contained within the implodable, the high pressure water contained between the implodable and the confining tube, the confining tube itself, and a small entrapped air bubble along the top edge of the confining tube.

3.1 Structural Model

The structural model, Fig. 3, consists of the implodable, confining tube and a mechanical indenter. The model is a one-quarter symmetry model consisting of symmetry planes oriented such that the model represents one-half of the length and one-half of the circumference of both tubes. Although the 31.8 mm and 50.8 mm diameter implodables collapse in a pure mode-2 shape which is 1/8 symmetric, the 76.2 mm implodable only exhibits 1/4 symmetry collapse and thus 1/4 symmetric models are used for consistency. The indenter is only part of the computational model and is not present in the actual experiment and is needed to initiate the collapse at the same pressure as seen in the experiments. The wall thickness variations and ovality imperfections in the implodables are not accounted for in the numerical model. Therefore the model is inherently stronger than the actual implodable and must be numerically initiated to correspond to the experiments.

Figure 3 Structural model of confining tube and implodable volume

Both the implodable and the confining tube are modeled with 80 quadrilateral shell elements around half the circumference and 200 elements along one-half on the length. This results in element sizes on the order of 0.6 mm for the implodable and 5 mm for the confining tube. Although the confining tube consists of larger elements, it is assumed to be sufficiently rigid due to its thickness such that deformations are minimal and the larger elements are acceptable. All structural elements utilize the Hughes-Liu element formulation with 5 integration points through the thickness of the elements. The outer surface of the implodable and the inner surface of the confining tube are modeled with appropriate shell offsets so as to capture the correct locations of the wetted surfaces. The steel confining tube is modeled as a linear isotropic material and the aluminum implodable is modeled with a “Rate-Dependent Tabular Isotropic Elastic-Plastic” material model, although rate dependency is not considered in the current model.

3.2 Fluid Model

The fluid model consists of the air internal to the implodable, the high pressure water contained within the confining tube, a small entrained air bubble at the top of the fluid body, and low pressure air external to the confining tube (see Fig. 4). The initial entrapped air bubble in the system is included due to the inability to fully evacuate all of the air from the confining tube in the experiments due to the horizontal orientation. The difference in pressures between the high pressure water and the ambient air contained within the implodable corresponds to the collapse pressure observed during the experiments and is unique to each geometry. Since the hydrostatic pressure recorded during the experiments is actually the gage pressure and not the absolute pressure in the fluid, the fluid pressure in the simulations corresponds to the observed collapse pressure plus the atmospheric pressure. By applying atmospheric pressure to the air inside the implodable, the same net pressure differential (collapse pressure) is achieved. For all geometries, the same fluid grid is utilized for consistency. The grid has fluid cell sizes of 0.75 mm in way of the implodable itself with the cells tapering to 1.5 mm in the radial direction towards the confining tube, and tapering to 2.5 mm in the axial direction towards the confining tube end-plates. In order to accurately capture the relatively small size of the bubble at the top of the confining tube, the cell size is refined to 0.2 mm at this location in the vertical direction. The fluid grid has a total of 20.8 million fluid cells. The air is modeled using an adiabatic, isentropic equation of state and the water is modeled with the compressible Tillotson equation of state. The sides of the grid that correspond to

symmetry planes have fixed reflecting boundary conditions and the other sides have free non-reflecting conditions.

Figure 4 Fluid model of air inside the implodable volume, high pressure water inside the confining tube and initial entrapped air bubble

3.3 Complete DYSMAS Model

In the computational models, the fluid and structural meshes are coupled through the use of interface element definitions which define the “wetted surface” of the structure. In the current models, doubly wetted interface elements are used in which fluid (water/air) can act on both faces of the shell elements. This is an important consideration since both the implodable and confining tube interface with both air and water. As these interface elements move through the fluid domain during the collapse of the implodable volume, air cells that are initially contained within the implodable

body can transition to water cells. The indenter is not included in the fluid structure interaction definition as the collapse has been initiated prior to the fully coupled run.

3.4 Loading / Collapse Initiation

In the computational models, the hydrostatic loading and subsequent collapse initiation are comprised of a two step process which occurs in an uncoupled preload simulation for computational efficiency. In the first step of the preload process, the cylindrical shell is hydrostatically loaded to the collapse pressure (gage pressure) observed in the respective experiments through a dynamic relaxation procedure in which the pressures are applied to the segment faces defining the water side surfaces of the implodable and confining tube. Once the dynamic relaxation phase converged, the cylindrical shell is squeezed upon by a radially inward moving indenter at a velocity of 60 cm/s. During this indentation phase, the position of the implodable's wall in way of the indenter is monitored until the wall separates from the indenter, which indicates the initiation of the collapse. At this point in time, the simulation is terminated and a file containing the relevant structural quantities (i.e. stresses, position, velocities) is written. This file is then used as the starting point for the structural component of the fully coupled DYSMAS simulation. It must be noted that for the fully coupled run, the mechanical pressures applied in the preload model are removed since the hydrostatic load is applied through coupling with the fluid domain in the coupled simulation. This process greatly reduces the computational resources needed to preload and initiate the collapse of the model. Although the same process could be applied in a fully coupled run, the computational time (cpu hours) would be

greatly increased due to the fluid domain cost which is not required during this phase of the model loading.

4. Results and Discussion

4.1 Experimental Results

The pressure profiles generated from the implosion of cylindrical shells inside the confining tube are captured for the experiments conducted on 31.8 mm, 50.8 mm and 76.2 mm outer diameter implodable volumes. These pressure histories are analyzed in detail in order to understand the mechanics of deformation of the implodables during the collapse. In subsequent sections, the analytical approaches in conjunction with experimentally observed pressure histories are utilized to predict the fluid motion inside the confining tube during the implosion. The energy redistribution of the initial hydrostatic potential energy is also discussed to distinguish between a rapid and a slow buckling process.

4.1.1 Evolution of Pressure Waves inside the Confining Tube

Underwater buckling of an implodable generates sudden dynamic deformation in the implodable walls, causing rapid motion and decrease in dynamic pressure in the surrounding fluid. This dynamic pressure drop causes development of low pressure waves. Similarly in the case of the implosion inside a confining tube, low pressure waves are emitted from the center of the implodable (or the center of the confining tube). These low pressure waves travel outwards in both axial directions towards the ends of the confining tube. Such pressure waves travel at a coupled pressure wave

speed, which has been widely studied in water hammer theory. Early studies by Korteweg (Korteweg, 1878) and Joukowsky (Joukowsky, 1900) have shown that the radial oscillation of a fluid filled tube is coupled with the longitudinal (axial) motion of the fluid and the coupled longitudinal pressure wave speed (c_f) in the fluid is given by

$$c_f = \frac{c}{\sqrt{1 + \frac{2KR}{Eh}}} \quad (1)$$

where the parameters are listed in Table 3.

Parameter	Value
ρ_f (Density of Water)	998.85 kg/m ³
K (Bulk Modulus of Water)	2.149 GPa
c (Wave Speed in Water)	1467 m/s
R (Inner Radius of the Tube)	88.9 mm (7.00 in)
E (Elastic Modulus of the Tube Material, SA106-B steel)	210 GPa
ν (Poisson's Ratio)	0.29
h (Thickness of tube wall)	19 mm (0.75 in)
L (Length of the tube)	2.13 m (84 in)

Table 3 Properties of the confining tube material and water

Recent investigation by Tijsseling (Tijsseling, 2007) estimated the coupled longitudinal pressure wave speed (c_f) in a fluid filled thick walled tube, which is given by

$$c_f = \left\{ \rho_f \left[\frac{1}{K} + \frac{2}{E} \left(\frac{R}{h} \left(1 - \frac{\nu^2}{1 + h/2R} \right) + \frac{1 + h/R}{2 + h/R} + \nu \right) \right] \right\}^{-1/2} \quad (2)$$

For the case of the confining tube geometry chosen in this study, both Korteweg's and Tijsseling's theory predict that the coupled longitudinal pressure

wave speed to be 1395 m/s. As the inner diameter of the confining tube (177.8 mm) and the unsupported length of the implodable (304.8 mm) are of the same order, the nature of the pressure waves in the vicinity of the implodable is approximately radial. Therefore, for the experimental determination of the pressure wave speed near the specimen, a spherical pressure wave front emission from the central part of the implodable is assumed as shown in Fig. 5(a). The path difference for the arrival of the pressure wave at each nearby transducer location was calculated by geometry as shown in Fig. 5(b).

Figure 5 Longitudinal pressure wave velocity estimation in the confining tube. (a) initial pressure wave development in the confining tube, (b) path difference calculation between ch-B and ch-C, and (c) longitudinal pressure wave velocity for 31.8 mm OD (geometry 1)

In Fig. 5(b), points B and C represent the location of transducers ch-B and ch-C in the confining tube. The pressure wave front is generated from point A of the implodable wall surface and reaches points B and M simultaneously. Therefore, in

order to reach point C , the pressure wave needs to travel distance MC , which can be calculated as,

$$MC = AC - AM = \left[\left[\frac{1}{2}(D-d) \right]^2 + l_1^2 \right]^{1/2} - \frac{1}{2}(D-d) \quad (3)$$

Table 4 shows the values of path difference (MC) between ch-B and ch-C for different geometries. The larger diameter implodable has greater path differences (relatively closer to l_1), which suggests an axial approximation near implodable is more accurate for larger implodables.

Geometry No.	Outer Diameter (d)	l_1	MC
1	31.8 mm (1.25 in)	76.2 mm (3.00 in)	32.52 mm (1.28 in)
2	50.8 mm (2.00 in)	76.2 mm (3.00 in)	35.69 mm (1.41 in)
3	76.2 mm (3.00 in)	76.2 mm (3.00 in)	40.78 mm (1.61 in)

Table 4 Path difference (MC) between ch-B and C for all geometries

The path differences between the transducers away from the implodable (i.e. Ch-1 to 4, Ch-5 to 8) was taken as their axial locations' difference as the radial nature of pressure waves can be neglected beyond the implodable location (assuming the wave becomes planar after travelling two diameters away from the center). Using these path differences, the wave speed inside the confining tube is calculated by estimating the time difference between the arrivals of pressure wave front at each respective transducer location and is shown in Fig. 5(c). As it can be seen from Fig. 5(c), the speed is in good agreement ($\sim 5\%$ error) with the Korteweg and Tijsseling predictions. It is important to note during this analysis the wave speed should be estimated before any reflected wave from the end-plates arrive at the transducer

locations. Therefore, pressure wave velocity is estimated in the central half axial length of the confining tube, which receives reflections only after 0.7 ms duration.

4.1.2 Pressure History Comparison

The pressure profiles observed in the confining tube for different geometries are shown in Fig. 6. Note that the pressure profiles observed in the implosion experiments exhibited symmetry, as the implodables are placed at the center of the confining tube. Time $t = 0$ is arbitrarily chosen prior to any dynamic pressure variations observed in the experiments. Each figure shows three pressure traces: 1) ch-B, which shows the pressure history at the center of the confining tube, 2) ch-6, which shows the pressure halfway between the implodable and the end-plate, and 3) ch-8, which shows the pressure history at the far end of the confining tube (end-plate). The ch-8 transducer is placed at center of the end plate whereas ch-B and 6 are flush mounted in the inner walls.

The theoretical critical buckling pressure calculated for the nominal dimensions of the geometry 1 using von-Mises theory is 3.72 MPa (540 psi) (Timoshenko and Gere, 1963; von Mises, 1929), while during experiments the collapse occurs at 4.14 MPa (600 psi). Such critical pressure variation can be attributed to small difference in the mean wall thickness of the commercially received tube specimens from the nominal value.

Figure 6 Pressure history in confining tube. (a) 31.8 mm OD implodable (geometry 1), (b) 50.8 mm OD implodable (geometry 2), and (c) 76.2 mm OD implodable (geometry 3)

As shown in Fig. 6, the initial decrease in the pressure at ch-B corresponds to the beginning of the collapse process. The buckling of the specimen occurs in mode-2 shape (two lobes) for geometry 1 and 2, while geometry 3 exhibits a single indentation only (this will be discussed later in Sec. 4.2). The buckling begins at the mid-span of the specimen, while the remaining specimen stays stationary. The initial buckling causes a local change in the volume of the specimen at the center. At the critical

buckling pressure, there is some excess volume of water present in the confining tube with respect to atmospheric pressure conditions, which maintains the hydrostatic pressure inside. This “excess compressible water” has two components: 1) actual compressible water present at high pressure and 2) the elastic expansion of the confining tube causing increased volume of water inside. If initial volume of the implodable ($V_{implodable_i}$) is assumed to be negligible as compared to confining tube volume ($V_{implodable_i} \ll \pi R^2 L$), the volume of excess compressible water (V_{excess_water}) can be estimated as following,

$$V_{water_i} = \pi R^2 L \left(1 + \frac{P_c R}{2hE} (5 - 4\nu) \right) - V_{implodable_i} \quad (4)$$

$$V_{excess_water} = V_{water_i} \left[1 + \frac{P_c}{K} \right] - (\pi R^2 L - V_{implodable_i}) \approx \pi R^2 L \left(\frac{R}{2hE} (5 - 4\nu) + \frac{1}{K} \right) P_c \quad (5)$$

Where V_{water_i} = the initial volume of water at the critical buckling pressure (P_c).

In the rightmost expression in Eq. (5), the first term represents the elastic expansion of the confining tube and second term is due to the compressibility of water. For the case of geometry 1, Eq. (5) shows that there is ~109 ml excess compressible water present in the confining tube and any comparable changes in the volume of the specimen with respect to 109 ml can decrease the overall hydrostatic pressure. In the case of geometry 1, it is observed that there is a rapid decrease of the pressure near the implodable, while the pressure near the end-plate remains constant. This decrease in the pressure of the fluid immediately surrounding the specimen is directly related to the inward velocity at which the shell surface is moving during the collapse. Therefore, during the buckling process, a pressure release wave in the surrounding

water is emitted from specimen's wall. Initially, this wave propagates spherically outwards and later axially towards the end-plate at coupled acoustic wave velocity causing the pressure to drop in the confining tube. These pressure release waves propagate the transient pressure disturbances in order to create spatially uniform hydrostatic pressure in the confining tube. For geometry 1 (see Fig. 5(a)), the initial pressure release wave emitted at $\sim t = 1.2$ ms reaches the end-plate at approximately $t = 1.9 \sim 2.0$ ms. It can be observed that the time scale of the pressure drop near the specimen (~ 0.6 ms) is smaller as compared to the $1/2$ time period of the pressure wave oscillation in the confining tube (~ 1.5 ms). So, the release wave front emitted by the geometry 1 is a short wavelength pressure pulse travelling outwards in the confining tube and it doesn't overlap with itself at the center.

It is interesting to see that the pressure drop at the end-plate (ch-8) is larger than that experienced at the center (ch-B). In the initial pressure decay region, the pressure at the end-plate almost reaches zero. This phenomenon is similar to the reflection of a high pressure wave from a rigid wall in water (virtually incompressible fluid), where the pressure imparted on the rigid wall is twice the magnitude of the incident pressure (Taylor, 1963). From the classical acoustic shock wave theory for water (Taylor, 1963) and assuming rigid reflection of the wave from the end-plate, the pressure decay rate almost doubles in magnitude in this case and a sharp pressure drop at the end-plate is observed (the increase in decay rate at the end-plate is apparent by looking ahead to Fig. 12). It can also be seen that in the initial collapse process, the release pressure wave emitted at the center has the strength of almost 75% of the initial hydrostatic pressure; therefore the end-plate encounters twice the magnitude of

the pressure wave (~ 150%). As a result, it causes a region of cavitation to develop at the end plate walls due to the inability of water to sustain tensile (negative) pressures. The presence of cavitation is indicated by the time duration of a zero pressure at the end-plate.

After the low pressure wave reflects from the end-plate, the net velocity of water still remains towards the center, which causes pressure to increase/maintain at the central region of the confining tube depending upon geometry. The resultant high-pressure waves reflect outward and eventually interface with the end-plates, which until this time had experienced zero pressure, as seen on transducers ch-1 and ch-8. These high pressure waves are similar to the classic water hammer wave studied in the opening/closing of valves in pipelines. In this case, the implosion of the specimen acts as *an opening of a check-valve* at the center of the confining tube, which develops a mean axial flow in the water towards the center. When the implosion process completes/arrests, the flow of water is restricted, representing *a sudden closing of a check-valve*, which is responsible for a high pressure hammer wave occurrence during these experiments. According to the water hammer theory, the time period of the hammer wave is $T = 4 \times (Length / Wave Speed)$ (Potter et al., 2002). Similarly in the case of the confining tube, the duration of the zero-pressure region can be written as,

$$T_{zero-pressure} \approx \frac{T}{2} = 2 \times \frac{Half\ Length\ of\ the\ Confining\ Tube}{Wave\ Speed\ in\ Water} = 2 \times \frac{1.067\ m}{1395\ m/s} = 1.530\ ms \quad (6)$$

After the development of these high pressure waves, the behavior of pressure inside the confining tube stays oscillatory. A schematic of such high/low pressure wave generation is also shown in Fig. 7. Please note that the local radial flow of water

near the center of the implodable is neglected in Fig. 7 and only the axial motion of water particles is shown for simplicity.

Figure 7 Schematic of implosion process in the confining tube

In the case of geometry 2 and 3, it is observed that the pressure drop at any location inside the confining tube is slower as compared to geometry 1 and decreases with increasing diameter (see Fig. 6(b) and 6(c)). Also in comparison to geometry 1, these geometries exhibit a relatively slower collapse and the collapse process was observed to lie between highly dynamic and quasi-static. As a result, the pressure waves have enough time to stabilize within the confining tube. The pressure near the specimen (ch-8) initially monotonically decays over time and doesn't exhibit any hump as observed in geometry 1.

In all the geometries, the onset of implosion generates low pressure acoustic waves from center. Later, further pressure drop at the center causes unsteady flow of water towards the implodable. The mechanism of the movement of water during initial collapse can be described as sink and source: 1) the end-plate region acts as a source of water, 2) the central region acts a sink of water. The net motion of water towards the implodable during the collapse process causes high pressure development at the center of the confining tube. This in turns leads to repeated high and low pressure wave oscillations after collapse finishes.

4.1.3 Wall Contact and Shock Generation

Free field hydrostatic implosion of cylindrical shells have shown that the contact made between opposing sides at the center of a collapsing cylinder generates a shorter duration small pressure spike followed by a longer duration large pressure spike, which is produced at the instant that contact between the opposing sides extends the full width of the cylinder (Turner and Ambrico, 2012) . These pressure spikes are locally generated from the implodable's surface and decay almost as a spherical wave

as a function of $1/r$. In the present study, such pressure spikes are also observed for geometry 1 (which exhibited full collapse/wall contact during experiments) and is shown in Fig. 8. At $t = 1.99$ ms in Fig. 8, a sharp pressure spike with a rise time of $5 \mu\text{s}$ is observed at ch-B indicating the initiation of specimen wall contact. The magnitude of the pressure spike is approximately 0.82 MPa with the time duration of $50 \mu\text{s}$. The quick depletion of hydrostatic pressure inside the confining tube during the implosion process is responsible for inhibiting the generation of a large pressure spike (above the initial hydrostatic pressure). The large pressure spike would be a result of the high pressure water rushing with high velocity against the walls of the implodable. In this case, the drop of hydrostatic pressure slows down the collapse process of the implodable. Thus, the surrounding low pressure water is incapable of generating a high pressure spike.

Figure 8 Pressure spike observed in geometry 1 (31.8 mm OD)

4.1.4 Pressure Spatial Distribution Analysis

In order to better understand the axial pressure changes that take place, a plot of pressure in the confining tube as a function of transducer location is generated for different instants of time in Fig. 9 for all the geometries.

In the case of geometry 1, a local pressure drop at the center of the confining tube occurs and travels as an acoustic wave axially towards the end-plate as shown in Fig. 9(a). During the time that the local pressure at the center decreases towards its minimum, there is no change in the pressure away from the center. This pressure drop gradually develops almost as a normal distribution and by time $t = 1.8$ ms, it reaches the lowest pressure value of 1.1 MPa. At this instant, the implodable reaches the highest wall velocity relating to the highest decrease in pressure. After this point, the implodable begins to decelerate and wall contact occurs leading to a sudden velocity arrest and high deceleration in the wall movement. As a result, a compressive high pressure wave is emitted and an increase in pressure is seen at the center. Therefore a combination of an initial release wave and generation of compressive wave leads to a double valley shaped pressure distribution (see $t = 2.0$ ms and 2.1 ms in Fig. 9(a)).

For Geometry 2 and 3, the spatial gradients of pressure are decreased due to slower pressure decay rates providing enough time duration for pressure wave oscillation, and hence, an effective global hydrostatic pressure drop is observed. The rate of pressure drop at the center is slower for geometry 2 and 3, as compared to geometry 1, due to slower wall velocity of the implodable volumes (see Fig. 9(b) and 9(c)). As a result, the pressure release wave from the center reaches the end-plate at $t = 2.1$ ms for geometry 2, while still maintaining high pressure (2.25 MPa, 63 % of the

Figure 9 Spatial distribution of pressure in the confining tube. (a) 31.8 mm OD implodable (geometry 1), (b) 50.8 mm OD implodable (geometry 2), and (c) 76.2 mm OD implodable (geometry 3)

buckling pressure) at the center. It is also interesting to note that after the collapse process, the pressure at the center of the implodable reaches an approximate constant pressure state and doesn't decay further. This implies that the velocity of the

implodable was really small and any pressure disturbances caused by the implodable can be ignored. The distribution of pressure for geometry 3 is very similar to geometry 2. The pressure wave evolution is even slower for geometry 3 with respect to geometry 2, leading to minimal pressure gradients.

4.1.5 Calculation of Change in Volume of the Implodable as a Function of Time

Using the pressure histories obtained at different axial locations, two approaches can be utilized to estimate the change in volume of the implodable as a function of time (collapse progression): 1) Mean hydrostatic pressure approach, in which the change in the mean hydrostatic pressure inside the confining tube is directly proportional to the change in volume of implodable, 2) Water particle velocity approach, in which the cumulative inward flow of water towards the central region of the confining tube represents the change in volume of implodable.

4.1.5.1 Mean Hydrostatic Pressure Approach

The implosion of cylindrical shells in a confined environment generates a local pressure drop as well as a global hydrostatic pressure drop across the entire confining tube. This mean hydrostatic pressure can be evaluated by spatially averaging the pressure at different axial locations and is plotted in Fig. 10(a). The initial pressure drop represents the collapse initiation in the specimens and this collapse progresses until the mean hydrostatic pressure reaches its first minimum value. For each case, it is evident from Fig. 10(a) that there is residual mean hydrostatic pressure wave present even after the end of collapse (> 5 ms). This is due to: 1) pressure oscillations: elastic recovery and vibration of the specimen generates mean hydrostatic pressure pulses in

the confining tube, and 2) residual pressure: a minimum critical pressure is required to progress the deformation of the specimen (Kyriakides and Babcock, 1981). If the mean pressure reaches below threshold value, no further deformation can be achieved. This phenomenon is similar to the ‘propagation pressure in pipelines’ reported earlier in the literature (Kyriakides and Babcock, 1981; Palmer and Martin, 1975).

In this analysis, the mean hydrostatic pressure is dominantly responsible for the deformation of the implodable. Also, the implodable is assumed to be in contact with water due to strong coupling between the water velocity and the implodable’s wall velocity throughout collapse duration.

The inner volume of the confining tube ($V_{confining}$) at a mean hydrostatic pressure $P_m(t)$, and the volume of water (V_{water}) at any time t can be written as,

$$V_{confining} = \pi R^2 L \left(1 + \frac{P_m(t)R}{2hE} (5 - 4\nu) \right); \quad V_{water} = V_{water_i} \left(1 + \frac{(P_c - P_m(t))}{K} \right) \quad (7)$$

Using $V_{water} + V_{implodable} = V_{confining}$,

$$V_{implodable} = \left[\pi R^2 L \left(1 + \frac{P_m(t)R}{2hE} (5 - 4\nu) \right) \right] - \left[V_{water_i} \left(1 + \frac{(P_c - P_m(t))}{K} \right) \right] \quad (8)$$

The change in the volume of specimen ($\Delta V_{implodable}$) can be written as a function of mean hydrostatic pressure

$$\Delta V_{implodable} = V_{implodable_i} - V_{implodable} = (P_c - P_m(t)) \left[\pi R^2 L \left(\frac{1}{K} + \frac{R}{2hE} (5 - 4\nu) \right) - \frac{V_{implodable_i}}{K} \right] \quad (9)$$

Using Eq. (9), the decrease in the volume of implodable as a function of time is plotted in Fig. 10(b). It can be seen that the change in volume of implodable for geometry 1, 2 and 3 is approximately 96 ml, 76 ml and 71 ml respectively. It is

striking that the smallest implodable exhibits the largest change in volume during implosion, while largest implodable exhibits the smallest change in volume. The reason being is that the extent of deformation (or $\Delta V_{implodable}$) depends upon V_{excess_water} present in the confining tube rather than the size of the implodable itself. Since $V_{excess_water} \propto P_c$ from Eq. (5), it implies that the extent of deformation will increase with increasing buckling pressure.

Figure 10 (a) Mean hydrostatic pressure in the confining tube as a function of time, and (b) $\Delta V_{implodable}$ as a function of using mean hydrostatic pressure approach

The V_{excess_water} in the confining tube for geometry 1, 2 and 3 respectively is ~109 ml, 87 ml and 91 ml (from Eq. (5)) and it is represented by the solid symbol in Fig. 10(b). It can be seen that a higher amount of V_{excess_water} leads to a higher $\Delta V_{implodable}$ during implosion and the difference between V_{excess_water} present in the system and $\Delta V_{implodable}$ for each experiment was approximately ~10-20% of V_{excess_water} . That there is a finite volume of V_{excess_water} present in the system after the collapse of the implodable is arrested indicates that there is a certain threshold of positive hydrostatic pressure below which the implosion process cannot continue. The difference between

the $V_{\text{excess_water}}$ and $\Delta V_{\text{implodable}}$ at the end of the event is discussed by Kyriakides and Babcock as the ratio of propagation pressure and critical buckling pressure (Kyriakides and Babcock, 1981). It concludes that the hydrostatic pressure cannot lead to positive work on the implodable, unless the mean hydrostatic pressure in the confining tube is larger than the propagation pressure (pressure required to continue the implosion process).

It is also interesting to note that the ratio of change in volume of implodable to the initial volume is significantly large for smaller implodable (~50%). This indicates that the small implodable is more likely to exhibit contact during implosion and this is consistent with the discussion in Sec. 4.1.3 that geometry 1 did exhibit wall contact.

4.1.5.2 Water Particle Velocity Approach

The implosion process occurring at the center of the confining tube can be considered as a sink of water where a net inward flow of water is generated during the collapse process. The volume of net flow of water towards the center is approximately equal to the deformation of the implodable. Hence, this approach for the calculation of $\Delta V_{\text{implodable}}$ relies on the estimation of axial particle velocity of water inside the confining tube using appropriate fluid mechanics field equations.

The following equations represent the generalized equations of conservation of mass and momentum used in fluid mechanics,

$$\frac{\partial \rho}{\partial t} + \bar{u} \cdot \nabla \rho + \rho(\nabla \cdot \bar{u}) = 0 \quad (10)$$

$$\rho \left(\frac{\partial \bar{u}}{\partial t} + \bar{u} \cdot \nabla \bar{u} \right) = -\nabla P \quad (11)$$

where ρ is the density of the water, P is the pressure in water and $\bar{u}$ represents the flow field of the water in Eulerian coordinates. Using the pressure-density relationship,

$$\frac{\partial P}{\partial t} = \left(\frac{\partial P}{\partial \rho} \right)_{\rho=\rho_0} \frac{\partial \rho}{\partial t} \quad (12)$$

Eq. (10) and (11) can be simplified as

$$\frac{1}{c_0^2} \left(\frac{\partial P}{\partial t} + \bar{u} \cdot \nabla P \right) + \rho (\nabla \cdot \bar{u}) = 0 \quad (13)$$

$$\rho_0 \left(\frac{\partial \bar{u}}{\partial t} + \bar{u} \cdot \nabla \bar{u} \right) + \nabla P = 0 \quad (14)$$

where $c_0 = \sqrt{\left(\frac{\partial P}{\partial \rho} \right)_{\rho=\rho_0}}$ is the wave speed in water at atmospheric pressure and

temperature. Although the flow of water at the center of the confining tube is a combination of inward radial and axial flow, the axial wave assumption is valid beyond the unsupported length of the implodable (See ‘dotted’ region in Fig. 11, $|x| > l/2$). Therefore the conservation equations can be further written as

$$\frac{1}{c_0^2} \left(\frac{\partial P}{\partial t} + u \frac{\partial P}{\partial x} \right) + \rho \frac{\partial u}{\partial x} = 0 \quad (15)$$

$$\rho \left(\frac{\partial u}{\partial t} + u \frac{\partial u}{\partial x} \right) + \frac{\partial P}{\partial x} = 0 \quad (16)$$

where x is the axis of the confining tube with the center at the origin and u represents the velocity of water in the x -direction. If the volumetric flow rate of water crossing

boundaries PQ and RS (in Fig. 11) towards the center is estimated, the accumulation of water ($\Delta V_{implodable}$) can be calculated as a function of time. Please note that in this approach, the compressibility of water in the central region is ignored and all the water crossing the boundary PQ and RS is considered to be preserving its volume.

Figure 11 Region of axial wave assumption in the confining tube

These governing equations, Eq. (15) and (16), couple the spatial distribution and time evolution of pressure ($P(x,t)$) as well as the fluid particle velocity ($u(x,t)$). Using the experimentally obtained pressure histories at different axial locations, a complete description of $P(x,t)$ can be estimated. Hence, the spatial pressure gradient

$\frac{\partial P}{\partial x}(x,t)$ and pressure change rate $\frac{\partial P}{\partial t}(x,t)$ can be obtained by differentiation

of $P(x,t)$. But, the coupled nature of these equations lead to high coupling

between $\frac{\partial P}{\partial t}$, $\frac{\partial P}{\partial x}$, u , $\frac{\partial u}{\partial x}$ and $\frac{\partial u}{\partial t}$. This implies that $u(x,t)$ cannot be explicitly

calculated from these equations easily. Therefore, $\frac{\partial u}{\partial t}$ is represented in terms of $\frac{\partial P}{\partial x}$,

$\frac{\partial P}{\partial t}$ and u by following steps:

$$\rho \left(\frac{\partial u}{\partial t} + u \frac{\partial u}{\partial x} \right) + \frac{\partial P}{\partial x} = 0 \quad (17)$$

$$\Rightarrow \rho \frac{\partial u}{\partial t} + u \left[-\frac{1}{c_0^2} \left(\frac{\partial P}{\partial t} + u \frac{\partial P}{\partial x} \right) \right] + \frac{\partial P}{\partial x} = 0 \quad (\text{Using Eq. (15)}) \quad (18)$$

$$\Rightarrow \rho \frac{\partial u}{\partial t} + \frac{\partial P}{\partial x} \left(1 - \frac{u^2}{c_0^2} \right) - \frac{u}{c_0^2} \frac{\partial P}{\partial t} = 0 \quad (19)$$

As the water particle velocity is very small as compared to the wave speed in water during the implosion process ($u \sim 25\text{-}50 \text{ m/s} \Rightarrow u/c_0 \ll 1$), the Eq. (19) can be rewritten as,

$$\Rightarrow \rho \frac{\partial u}{\partial t} = \frac{u}{c_0^2} \frac{\partial P}{\partial t} - \frac{\partial P}{\partial x} \quad (20)$$

The plot of $\frac{\partial P}{\partial x}$ and $\frac{\partial P}{\partial t}$ obtained at different times as a function of axial location is shown in Fig. 12.

The spatial pressure gradient ($\frac{\partial P}{\partial x}$) exhibits the highest value of 7 MPa/m for the case of geometry 1, while other geometries exhibit relatively smaller $\frac{\partial P}{\partial x}$ of 2.5 MPa/m and 1.9 MPa/m for geometry 2 and 3 respectively. The pressure gradients for geometry 1 develop quickly and propagate as a wave towards the ends of the confining tube, while the other two geometries exhibit slow development, leading to reflections from the ends.

Similar to $\frac{\partial P}{\partial x}$, the pressure decay rate ($\frac{\partial P}{\partial t}$) at the center also exhibits the highest value of 9 GPa/s for geometry 1, while both geometry 2 and 3 show $\frac{\partial P}{\partial t}$ of ~ 3 GPa/s and 2.5 GPa/s. The slow $\frac{\partial P}{\partial t}$ for geometry 2 and 3 is a direct indication of slow

Figure 12 Spatial distribution of $\frac{\partial P}{\partial x}$ and $\frac{\partial P}{\partial t}$ in the confining tube. (a) 31.8 mm OD implodable (geometry 1), (b) 50.8 mm OD implodable (geometry 2), and (c) 76.2 mm OD implodable (geometry 3)

collapse progression. At the end-plates, the maximum $\frac{\partial P}{\partial t}$ was ~ 13 GPa/s, 5 GPa/s

and 4 GPa/s for geometry 1, 2 and 3 respectively, which is almost twice the magnitude

encountered in each case at the center indicating rigid reflection of pressure wave at the end-plates discussed in Sec. 4.1.2 (Taylor, 1963).

Using the following initial conditions (Eq. 21) for Eq. (20), the water velocity $u(x, t)$ at different axial locations as a function of time can be obtained by iteratively solving for u in Eq. (18) using a finite difference scheme.

$$u(x, t = 0) = 0; \quad P(x, t = 0) = P_c \quad (21)$$

This approach for estimating $u(x, t)$ is simple, as it only utilizes initial conditions of the problem and no boundary conditions are required at the end-plates. It eliminates the issue of characterizing the complex boundary condition present at the end-plate (stress-free boundary condition after cavitation, zero-velocity rigid boundary condition otherwise). The time histories of water velocity for the three geometries at different axial locations are plotted in Fig. 13. This figure shows the maximum water velocity (PQ/RS , From Fig. 11) achieved for geometry 1 is 2.2 m/s (at 1.85 ms), while it is 1.69 m/s (at 2.9 ms) for geometry 2 and 1.32 m/s (at 3.37 ms) for geometry 3, respectively. The total volumetric flow rate of water accumulated in $PQRS$ region or $\Delta V_{implodable}$ can be written as

$$\Delta V_{implodable}(t) \approx \int_0^t \left[(|u_{PQ}| + |u_{RS}|) \times \frac{\pi}{4} (D^2 - d^2) \right] dt \quad (22)$$

where $|u_{PQ}|$ and $|u_{RS}|$ represent the magnitude of water velocity at location PQ and RS respectively. The plot of $\Delta V_{implodable}$ as a function of time is shown in Fig. 14. It can be seen that the estimation of $\Delta V_{implodable}$ from water velocity approach is similar to mean hydrostatic pressure approach as shown in Fig. 10(b).

Figure 13 Water velocity $u(x,t)$ as a function of time. (a) 31.8 mm OD implodable (geometry 1), (b) 50.8 mm OD implodable (geometry 2), and (c) 76.2 mm OD implodable (geometry 3)

Figure 14 $\Delta V_{implodable}$ as a function of time for all geometries from water velocity approach

4.1.6 Transient Collapse Model for Predicting Complete Deformation History of Cylindrical Shells

During the progression of the collapse of a cylindrical shell prior to wall contact, there is a specific mode associated with an initial given geometry. This collapse mode primarily defines the deformation of the shell during collapse process (note that the high order non-volume preserving vibration modes have been ignored). Hence, the extent of collapse ($\Delta V_{implodable}$) in cylindrical shells can be expressed as a function of the deformation at a single point (r_1, θ_1, z_1) (the mid-point in this case) on the shell uniquely for a specific geometry.

$$\Delta V_{implodable}(t) = f(\bar{U}(r_1, \theta_1, z_1, t)) \quad (23)$$

This deformation model is generally a nonlinear function for a specific geometry hence it is numerically evaluated in this study for all the geometries. A dynamic buckling simulation of the implodables (without fluid) chosen in this study was performed using DYSMAS and the internal volume of the implodables was estimated as a function of mid-point displacement. A typical deformation model calculated is shown in Fig. 15(a), in which $\Delta V_{implodable}$ for all the geometries used in this study is expressed as a function of mid-point displacement. It is noted from Fig. 15(a) that for both 31.8 mm and 50.8 mm OD, the initial change in volume can be approximately represented as a quadratic function of mid-point displacement. It is interesting to note that the $\Delta V_{implodable}$ observed for 50.8 mm OD has a very similar trend as the 31.8 mm OD up to ~16 mm displacement. As $\Delta V_{implodable}$ has been shown to be estimated by both mean hydrostatic pressure approach and water particle

velocity approach, the mid-point deformation of the implodable can be back calculated using this deformation model ($\bar{U}(r_1, \theta_1, z_1, t) = f^{-1}(\Delta V_{implodable}(t))$). This model is further used to predict the time evolution of mid-point velocity of the implodable and is shown in Fig. 15(b). It is clear that the maximum velocity achieved during implosion is largest for geometry 1 (~25 m/s). Geometry 1 also exhibits the largest accelerations, which is characteristic of sudden collapse of a structure. On the contrary, geometry 2 exhibits a smaller maximum velocity of 12 m/s and ~ 5 times slower accelerations. Hence, it can be inferred that the implosion of larger geometries in a confining space is a slow process and the velocities achieved in such cases are much smaller as compared to a free-field natural implosion.

It should be noted that the transient deformation model for geometry 3 is not discussed here because it didn't exhibit the evolution of mode-2 collapse and just an indentation was observed during the confined tube implosion experiments.

Figure 15 (a) $\Delta V_{implodable}$ as function of mid-point deflection, and (b) calculated mid-point velocity of implodable

4.1.7 Redistribution of Energy in the Confining Tube

In the implosion experiments conducted inside a confining tube, the total energy of the whole system, i.e. confining tube + water + implodable + air inside implodable, should be constant during the collapse process because the system is isolated and there is no flow of energy from outside in the system. Therefore, the energy balance between the initial state (prior to the onset of implosion) and at any time $t = t_1$ can be written as,

$$E_{t=0} = E_{t=t_1} + E_{loss} \quad (24)$$

$$E_{confining}\Big|_{t=0} + E_{water}\Big|_{t=0} = E_{confining}\Big|_{t=t_1} + E_{water}\Big|_{t=t_1} + E_{implodable}\Big|_{t=t_1} + E_{air}\Big|_{t=t_1} + E_{loss} \quad (25)$$

It can be noted in Eq. (25) that the initial energy terms for the implodable and the air have been omitted because at the onset of the implosion process, there is no significant deformation in the implodable causing no change in these energies.

Different energies present in the water and structure can be estimated as follows:

- The energy in the confining tube

$$E_{confining}\Big|_{t=t_1} = \frac{1}{2}(\sigma_{\theta\theta}\varepsilon_{\theta\theta} + \sigma_{xx}\varepsilon_{xx})V_{confining_shell} = \frac{\pi R^3 P_m(t_1)^2 L}{Eh} \left[\frac{5}{4} - \nu \right] \quad (26)$$

where σ_{xx} = axial stress; ε_{xx} = axial strain; $\sigma_{\theta\theta}$ = hoop stress; $\varepsilon_{\theta\theta}$ = hoop strain;

$V_{confining_shell}$ = volume of confining tube material.

- The energy in the water

$$E_{water} \Big|_{t=t_1} = Potential\ Energy(P.E_{water}) + Kinetic\ Energy(K.E_{water})$$

$$P.E_{water} \Big|_{t=t_1} = \frac{\pi R^2 L P_m (t_1)^2}{2K}; \quad K.E_{water} \Big|_{t=t_1} = \frac{\rho \pi R^2 L}{2} u_{rms}^2(t_1)$$
(27)

Where $u_{rms}(t_1)$ = root mean square velocity inside the confining tube =

$$\left[\frac{1}{L} \int_{-L/2}^{L/2} u^2(x, t_1) dx \right]^{1/2}$$

- The energy absorbed in elasto-plastic deformation of implodable: This energy is taken to be the strain energy absorbed through elasto-plastic deformation of the implodable's walls during collapse. This strain energy of the cylinder walls is output explicitly from the DYSMAS simulation throughout time and it is estimated by the volume integration of the strain energy per unit volume. A detailed discussion of the calculation of the strain energy in the walls of an implodable during collapse is provided by Chamberlin et al (Chamberlin et al., n.d.). $E_{implodable}$ calculated from DYSMAS was investigated as a function of mid-point displacement and the calculations of mid-point displacement shown in Sec. 4.1.6 was used to track the $E_{implodable}$ as a function of time.

- The energy present in air

$$E_{air} \Big|_{t=t_1} = P_{atm} V_{specimen_i_air}^\gamma \left[\frac{V_{specimen_air}^{1-\gamma}(t_1) - V_{specimen_i_air}^{1-\gamma}}{1-\gamma} \right]$$
(28)

$V_{specimen_i_air}$ = volume of air inside specimen before implosion

$V_{specimen_air}(t_1)$ = volume of air inside specimen at time $t = t_1$

P_{atm} = atmospheric pressure = 0.101 MPa

γ = adiabatic index for air = 1.4

- Although the confining space in the tube is highly energy efficient, energy losses are present in the system and these include viscous losses, phase change/cavitation energy loss, and frictional losses in the implodable at wall contact and at end caps of implodable.

The plot of all these energies as a function of time for geometries 1 and 2 is shown in Fig. 16. It is evident from Fig. 16 that the initial hydrostatic potential energy decays rapidly during the implosion process. A major part of initial hydrostatic potential energy in water transforms into the work done in the elasto-plastic deformation of the implodable and the kinetic energy of the water. The rapid collapse of geometry 1 leads to large movement of water causing high kinetic energy in the water (~ 90 J), while the slow collapse of geometry 2 leads to small kinetic energy in the water (~ 42 J). Still in both of the cases, it was seen that a significant portion of the initial hydrostatic potential energy transforms into the elasto-plastic deformation of the implodable. In these experiments, the energy going into the adiabatic compression of the air inside the implodable and the elastic energy contribution from the confining tube as shown in Fig. 16 is found to be very small as compared to other energies. The calculation of E_{loss} using Eq. (25) also shows that there is relatively small energy loss (7-13%) at the end of the implosion process.

With the combination of velocity of the implodable and the energy redistribution, it can be established that there are two types of implosions that could

occur within a confining tube: 1) rapid implosion and 2) slow/partial implosion. Rapid implosion occurs when the energy available in the confining environment is much larger as compared to plastic dissipation energy of a completely collapsed implodable. These implosions are characterized by rapid energy transfer from surrounding water to the implodable, which also leads to significant transfer of energy into kinetic energy of water ($\sim 50\%$). On the contrary, slow implosion occurs when the energy available in the confining environment is smaller than the plastic dissipation energy of a collapsed implodable. In this case, most of the potential energy available transfers into plastic dissipation in the implodable leading to negligible transfer into kinetic energy of water ($< 20\%$). With this approach, a new deformation model can be formulated where the deformation of the implodable for slow implosion can be approximated by the energy balance between the loss of hydrostatic potential energy and the gain in total deformation energy (elastic and plastic) of the implodable. This methodology holds accurate until the kinetic energy gained during implosion is not negligible as compared to initial hydrostatic potential energy.

Figure 16 Energy redistribution inside confining tube. (a) 31.8 mm OD implodable (geometry 1), and (b) 50.8 mm OD implodable (geometry 2)

4.2 Comparison of Computational Results

The computational models that have been developed for each of the respective implodable geometries are correlated to the experimental results utilizing available experimental data, specifically the pressure time histories and final collapsed shapes of the specimens. Additionally, based on acceptable correlation between the experimental data and simulation results, further observations are made consisting of the pressure fields in the full fluid domain and the collapse progression of the implodables.

Comparisons of the experimental and computational pressure histories for the centerline gage (ch-B) and the end cap gage (ch-8) are provided in Fig. 17. From these figures it is seen that at both the centerline and the end cap locations there is good agreement between the simulations and the experiments. Specifically the computational models accurately predict both the rate of pressure drop during the implosion as well as the magnitude of the drop. Furthermore, the simulations are able to predict the onset, and time duration, of cavitation occurring at the end caps of the confining tube as indicated by the zero pressure region in the ch-8 figures. The models also predict the development of a pressure pulse occurring at the end caps subsequent to the cavitation development, although in the simulation results the pressure pulse does occur at slightly different points in time as compared to the corresponding experiment with the 31.8 mm and the 50.8 mm showing a delayed pressure development and the 76.2 mm simulation predicting an earlier development. Similar levels of correlation are seen for all pressure gage comparisons (ch-2 to 4, ch-A) and are omitted for brevity in the current discussion.

(a)

(b)

(c)

Figure 17 Comparisons of experimental and numerical pressure histories in the confining tube. (a) 31.8 mm OD implodable (geometry 1), (b) 50.8 mm OD implodable (geometry 2), and (c) 76.2 mm OD implodable (geometry 3)

Figure 18 shows the comparison of collapsed shapes between the experiments and the simulations. From the side-view figure comparison, it is seen that the computational models are able to accurately capture the mode shape of the collapse but the extents of the collapse progression for computational simulations are slightly smaller as compared to the experimental results. This is because of the later effects (> 10 ms) of the pressurization (due to the pump running for a finite amount of time after the implosion), which leads to minor additional collapse of the implodable during experiments.

Specifically, the models predict a mode-2 collapse for all models with the 31.8 mm geometry collapse progressing nearly the full length of the implodable, which agrees well with the experimental results. By comparison the 50.8 mm geometry does not fully collapse in the simulation and the collapse is arrested prior to the opposing walls impacting each other. Finally, although the 76.2 mm implodable does initially begin to collapse, due to the effects of the hydrostatic pressure, the collapse only begins on one side of the implodable and is arrested prior to the opposite side obtaining any inward motion. This is likely due to a sufficiently rapid drop in surrounding pressure that the driving force of the implosion is quickly removed. Similar one side collapse for 76.2 mm was observed in experimental results.

Cross sectional views of the final collapsed shape for each implodable are also provided in Fig. 18 which show the relative progression inward of the centerline of the volumes.

In order to verify the predictions of analytical models proposed in Sec. 4.1.5.1, a comparison between analytically and numerically evaluated $\Delta V_{implodable}$ is performed.

It is seen that the value of $\Delta V_{implodable}$ from numerical simulations is ~ 92 ml and 70 ml for geometry 1 and 2 respectively, while analytical calculations predict ~ 96 ml (4% difference) and 76 ml (8.5% difference) for the corresponding geometries. Hence, it can be concluded that the proposed analytical approaches can accurately predict the extent of deformation in confined implosion processes.

Figure 18 comparison of final collapse shape of the implodable. (a) 31.8 mm OD implodable (geometry 1), (b) 50.8 mm OD implodable (geometry 2), and (c) 76.2 mm OD implodable (geometry 3)

Figure 19 and 20 show the collapse progression and resulting fluid field pressure contours for the 50.8 mm implodable simulation. From the structural collapse evolution it is seen that although the implosion is initiated by indenting the top surface of the structure, the structural instability quickly causes both sides to gain radially inward motion as expected. For the case of both the 50.8 and 76.2 mm implodable this inwards motion is ultimately arrested prior to the opposing surfaces impacting at the horizontal plane. From Fig. 20, it is seen that when the implosion is initiated and the walls begin to move inwards there is an associated drop in surrounding fluid pressure. This pressure drop begins at the axial plane of symmetry and then progresses towards the end cap in a form of a release wave as water rushes towards the center to fill the void resulting from the inward motion of the implodable walls. This axial progression of the pressure drop is also displayed in the time histories in that there is a time delay between the initial pressure drop at the center gage and the pressure drop at the end cap gage. Since there is a fixed volume of water within the confining tube and there is no available reservoir of fluid to maintain the hydrostatic pressure in the system, there is also a net decrease in the hydrostatic pressure of the surrounding fluid. It is this hydrostatic pressure which is required to continue driving the collapse of the implodable, and when this driving force drops below a certain critical level the implosion will be arrested. From the pressure field contours of 5 and 5.5 ms, it is seen that the net hydrostatic pressure in the fluid body is approximately less than 0.45 MPa (less than 15% of the initial collapse pressure of 3.44 MPa).

Figure 19 Progression of collapse for 50.8 mm OD implodable from simulations

Figure 20 Fluid pressure contours inside confining tube for 50.8 mm OD implodable volume from simulations

5. Conclusions

An experimental study along with computational simulations was conducted to understand the implosion mechanics inside a confining environment. The key findings of this study are as follows:

- (1) The limited energy present inside the confining tube significantly influences the implosion process. The normalized extent of collapse (i.e. $\Delta V_{implodable} / V_{implodable_i}$) is larger for the small diameter implodable compared to larger implodable volumes. In each case, the change in volume of the implodable ($\Delta V_{implodable}$) is approximately 80-90% of the excess compressible water present inside the confining tube.
- (2) Pressure history inside the confining tube can be described as a) a local decrease in pressure near the implodable, generating a pressure release wave travelling outwards to the end-plates. b) The release wave rapidly decreases the pressure at the end-plates to zero-pressure, while the pressure is maintained at the center. c) The high pressure wave generated at the center hits the end-plates leading to high pressure hammer wave.
- (3) The pressure gradients and the pressure decay rate decrease within the confining tube with increasing implodable diameter. In turn, the average axial particle velocity of water inside the confining tube decreases with increasing diameter implodable.
- (4) The initial potential energy present in the water transforms into two parts: a) elasto-plastic work done on the implodable and b) kinetic energy in the water. The small diameter implodable exhibits rapid collapse process, while large implodable

diameters exhibit slower collapse. This results into higher kinetic energy in water during the implosion of small implodable diameters as compared to large diameter implodables.

(5) Computational simulations using coupled Eulerian-Lagrangian approach can accurately predict the pressure histories as well as the collapse progression of a confined implosion event. The evolution of implosion process, the resulting fluid motion, the pressure waves' generation, and the hammer wave are distinctly captured in the simulations.

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CHAPTER 6

IMPLOSION OF LONGITUDINALLY OFF-CENTERED CYLINDRICAL VOLUMES IN A CONFINING ENVIRONMENT

by

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ABSTRACT

A comprehensive experimental/numerical study on the implosion of longitudinally off-centered cylindrical implodable volumes was conducted within a tubular confining space. In particular, the aim of this study was to examine the changes in the implosion mechanics and in the nature of pressure waves, arising from the longitudinally off-centered location of the implodable volume. Experiments were conducted with 31.8 mm outer diameter, cylindrical aluminum 6061-T6 implodable volumes placed concentrically within the confining tube. Three longitudinal offset locations were chosen within the confining tube, such that distance from the center of the implodable volume to the center of confining tube is equal to: (a) zero (b) $3/7$ of the half-length of confining tube (L), and (c) $5L/7$. Pressure transducers mounted on the inner surface of the confining tube were used to capture the pressure waves released during the implosion event. Computational simulations were performed using a coupled Eulerian- Lagrangian scheme to explicitly model the implosion process of the tubes along with the resulting compressible fluid flow. The experiments revealed that the longitudinal asymmetric placement of the implodable volume enhances the strength of hammer pressure waves generated during the implosion process. The off-centered location of the implodable volume causes a pressure imbalance in the entire length of the confining tube. Hence, the water particle velocity shifts towards the implodable volume producing high pressure region at the end-plate near the implodable volume, while the other end-plate experiences significantly longer cavitation due to low pressure. This far end-plate cavitation duration is also found to increase with increasing longitudinal offset, even though the total combined cavitation

duration at both the end-plates is approximately same for all offset locations. With high correlation observed between the experiments and simulations, computation models were further used to correlate the longitudinal offset and the signature of pressure waves at various interpolated locations. Simulations show that there is increase in both the peak pressure and the impulse of the hammer wave with increasing longitudinal offset of the implodable volume. Simulations also show that the collapse rate of the implodable volume decreases with the increasing longitudinal offset.

Keywords

Implosion, Implodable Volume, Collapse, Pressure Waves, Computational Modeling, Fluid Structure Interaction, Water Hammer, Confined Environment, Longitudinal Offset

1. INTRODUCTION

Natural hydrostatic underwater implosion of longitudinally off-center metallic cylindrical shells within a tubular confining space is investigated. In particular, the aim of this study is to examine the changes in the implosion mechanics arising from the relative longitudinal location of the implodable volume within the confining tube. The implodable volumes consist of aluminum 6061-T6 cylindrical tubing, and are placed at three longitudinal locations within the confining tube concentrically. The pressure histories generated by the implosion event are captured by dynamic pressure transducers mounted on the inner surface of the confining tube.

Cylindrical metallic shell structures are extensively used in the design of numerous underwater structures, such as deep ocean submersibles, submarines, underwater remote operated vehicles, underwater pipelines etc. [1–3]. In certain situations, these structures are required to contain a lower, uncompensated, pressure gas (or gas at atmospheric pressure) inside. The increasing external pressure of water at extreme sea depths can cause the enclosed shell to become structurally unstable and thus causing the structure to collapse onto itself. The resulting collapse of the structure is highly violent and it results in a rapid release of energy in the form of strong shock waves, high velocity fluid motion, and sound [4]. For this reason, the problem of “implosion” has been a topic of great interest to naval and marine communities [5–7,3]. Interest in underwater implosion mechanics primarily began in the 1950’s, when Issac and Maxwell showed that the implosion of glass spheres can be utilized as a deep-sea acoustic signaling device [5]. Later, Urick conducted free-field experiments with glass bottles to characterize the nature of the implosion pressure pulse for various sizes of implodable volumes [6]. Vanzant et al. studied the implosion of aluminum spheres and found that the impulse of the pressure pulses agrees well with the Raleigh’s gas bubble collapse theory [8], although the experimental peak pressures were lower as compared to the theoretical values due to the time taken in the failure of the structure [9]. Orr and Schoenberg, and Harben and Boro, both experimented with the implosion of single and bundled glass spheres to explore the possibility of using it as a broadband underwater acoustic source [10,11]. The failure of 7000 photomultipliers tubes in an accident at super Kamiokande laboratory is considered a

classical example of the detrimental nature of the shock waves generated during underwater implosion events [12].

Recently, there have been few studies reported investigating the mechanics of free-field underwater implosions [7,3,13–18]. Turner measured the near-field pressure history during the implosion of glass spheres and conducted corresponding numerical modeling using the coupled fluid-structure interaction code, Dynamic System Mechanics Analysis Simulation (DYSMAS) [7]. Turner concluded that the failure time history of the structure has a significant influence on an implosion pressure pulse. Later, both Turner and Ambrico, and Farhat et al. studied the implosion of aluminum cylindrical tubes experimentally and numerically [3,13]. Turner and Ambrico identified the key features of a typical mode-2 implosion process by correlating the important signatures of the pressure pulse with the structural deformation [3]. Farhat et al. extended this work by comparing both mode-4 and mode-2 implosion, and showed that the higher mode of collapse generates a higher pressure peak but of smaller duration compared to that obtained in a lower mode collapse [13]. Recently, Gupta et al. utilized a high speed digital image correlation technique to capture the real-time deformation of the implodable volumes, and showed that the time period and impulse of the positive pressure wave directly correlates to the time taken for the collapse to propagate along the length of the implodable volume [15].

The previously discussed experimental and numerical implosion studies are conducted in a free-field environment, where the net hydrostatic pressure in the surrounding fluid is maintained during the implosion process and the free-field environment acts as an infinite source of hydrostatic potential energy to drive the

implosion process. On the contrary, in the case of implosions occurring within a confining space, the source of hydrostatic potential energy to progress the implosion is limited. Recently, the authors performed a comprehensive investigation on the implosion of cylindrical shells within a tubular confining environment [19]. The implosion of three different diameter specimens ranging between 31.8 mm to 76.2 mm with approximately similar collapse pressures was studied. In this study, longitudinally centered implodable volumes were placed concentric to the confining tube. Such symmetric placement of the implodable volume about the center of the confining allowed for generation of *longitudinally symmetric low pressure waves* later evolving into the symmetric high pressure hammer waves. It was concluded that the limited hydrostatic potential energy available in a confining environment significantly influences the implosion process. The speed and the extent of collapse progression are largely affected by the sudden decrease of potential energy during implosion within confining tube. The findings of this investigation were limited to the implosion of longitudinally centered implodable volumes and were aimed to understand the effect of the relative size of the implodable volume with respect to the confining tube on the implosion mechanics. Therefore, the current study focuses on understanding the effect of longitudinally off-centered placement of implodable volumes on the nature of pressure waves generated. An effort is made to distinguish the fundamental differences in the mechanics between symmetrically and asymmetrically placed implosions using both experiments and numerical simulations. The results revealed that the asymmetric placement of implodable in the confining tube enhances the impulse of hammer pressure waves generated in the implosion process by 3 to 4 times.

2. EXPERIMENTAL SETUP

A schematic of the experimental setup for the tubular confined implosion facility is shown in Fig. 1 (a detailed description of this facility can be found in [19]). The confining tube has an inner diameter ($2R$) of 178 mm (7 in) with 19 mm (0.75 in) wall thickness (h) and is made out of seamless low-carbon steel (SA106-B) pipe. The overall internal length ($2L$) of the confining tube is 2.13 m (84 in). High frequency dynamic pressure transducers (PCB-113B22) are installed in the wall of the vessel at various axial locations such that the transducer sensor element is flush with the inner surface (see Fig. 1 for exact locations).

Figure 1 Schematic of the experiment setup. Distance between the center of the confining tube and the center of the implodable volume equals to (a) zero, (b) $3L/7$ and (c) $5L/7$.

The implodable volumes used in this study are comprised of commercially available aluminum 6061-T6 extruded seamless tubing. The unsupported length (l) of the implodable volumes is 305 mm (12 in). A schematic of the mounting fixture for holding the implodable volume is shown in Fig. 2. Table 1 provides a summary of the experiments conducted and the details of the specimen used in this study. Ovality parameter ($\Delta_0 = (d_{\max} - d_{\min}) / (d_{\max} + d_{\min})$) and wall eccentricity parameter ($\Xi_0 = (t_{\max} - t_{\min}) / (t_{\max} + t_{\min})$) are measured for each specimen to quantify the initial imperfections present in the specimen before experiments and are also shown in Table 1 [20]. The implodable volumes are sealed on both ends using solid aluminum end-caps, which utilize circumferential o-rings for sealing the specimen for underwater experiments. This tube and end-cap assembly is mounted inside the confining tube using support collets, which ensure concentric placement of the implodable volume in the confining tube (see Fig. 2). The construction of the three spoke collet is chosen so as to provide minimal interference to the axial pressure waves and water flow within the confining tube during the implosion event. The confining tube is filled and pressurized with water using a hydrostatic test pump. For each longitudinal offset location, at-least two experiments are conducted for repeatability.

Figure 2 Schematic of the implodable volume with end-caps and support collets

To investigate the effect of longitudinal off-centered placement of implodable volume on the implosion mechanics, the implodable volumes are placed at three different offset locations such that the distance between the center of the implodable volume and the center of the confining tube is equal to zero, $3L/7$ and $5L/7$. A schematic of the each experiment conducted is also shown in Fig. 1.

Longitudinal Offset	Average Wall Thickness (t)	Outer Diameter (d)	Wall Eccentricity (Ξ_0)	Ovality (Δ_0)	Experimental Collapse Pressure (P_c)
0	0.904 mm (0.0356 in)	31.78 mm (1.2512 in)	0.7%	0.01%	4.08 MPa (592 psi)
$3L/7$	0.902 mm (0.0355 in)	31.78 mm (1.2512 in)	0.7%	0.02%	4.08 MPa (592 psi)
$5L/7$	0.904 mm (0.0356 in)	31.78 mm (1.2510 in)	0.7%	0.04%	4.07 MPa (590 psi)

Table 1 Layout of the experiments conducted in this study

3. COMPUTATIONAL MODEL

Computational models of the implosion experiments are developed using the DYSMAS software, maintained by the Naval Surface Warfare Center (NSWC), Indian Head Division. The software is a fluid-structure interaction (FSI) code that consists of a structural solver (ParaDyn), a fluid solver (GEMINI), and a standard coupler interface. ParaDyn is an explicit Lagrangian solver suitable for large deformation dynamic problems. GEMINI is an explicit Eulerian fluid solver with equation of state (EOS) models for gases, liquids, explosives, and solids. The standard coupler interface allows the fluid and structural codes to share the required variables for the fluid structure interaction problem.

The computational models are developed to (1) capture the fluid structure interaction between the implodable volume and the surrounding fluid, (2) predict the pressure time history resulting from the implosion and, (3) predict the final collapsed shape of the implodable volume. The models include the implodable volume, the air contained within the implodable volume, the high pressure water contained between the implodable volume and the confining tube, the confining tube itself, and a small entrapped air bubble along the top edge of the confining tube. Please note that the methodology of these computational models is similar to shown in [19], therefore only a brief overview of the model is presented here.

3.1 Structural Model

The structural model, Fig. 3, consists of the implodable volume, confining tube and a mechanical indenter. The model is a one-half symmetry model with the symmetry planes oriented such that the model represents one-half of the circumference of both tubes. The indenter is only part of the computational model and is not present in the actual experiment and is needed to initiate the collapse at the same pressure as seen in the experiments. The effects of ovality and wall eccentricity are not included in the numerical model and as such the model is computationally “stronger” than the corresponding experimental specimen.

Both the implodable volume and the confining tube are modeled with 120 quadrilateral shell elements around half the circumference and 400 elements along the length. The steel confining tube is modeled as a linear isotropic material and the aluminum implodable volume is modeled with a “Rate-Dependent Tabular Isotropic Elastic-Plastic” material model, although rate dependency is not considered in the

current model. The outer surface of the implodable volume and the inner surface of the confining tube/end-plates are modeled so as to capture the correct wetted interfaces. Hughes-Liu shell elements are used for all elements with the appropriate shell offsets applied.

Figure 3 Structural model of the confining tube and implodable volume ($5L/7$ offset shown)

3.2 Fluid Model

The fluid model consists of the air internal to the implodable volume, the high pressure water contained within the confining tube, a small entrained air bubble at the top of the fluid body, and low pressure air external to the confining tube (see Fig. 4). The initial entrapped air bubble in the system is included due to the inability to fully evacuate all of the air from the confining tube in the experiments due to the horizontal orientation. The grid has fluid cell sizes of 0.75 mm in way of the implodable volume itself with the cells tapering to 1.5 mm in the radial direction towards the confining

tube, and tapering to 2.5 mm in the axial direction towards the confining tube end-plates. In order to accurately capture the relatively small size of the bubble at the top of the confining tube, the cell size is refined to 0.2 mm at this location in the vertical direction. The fluid grid has a total of 44.9 million fluid cells. The air is modeled using γ law equation of state, in which changes in pressure and volume are accomplished with a reversible, adiabatic process. The water is modeled with the compressible Tillotson equation of state. The side of the grid corresponding to the symmetry plane has a fully reflecting boundary condition and the other sides have free non-reflecting conditions.

Figure 4 Fluid model of air inside the implodable volume, high pressure water inside the confining tube and initial entrapped air bubble for centrally placed implodable volume

3.3 Complete DYSMAS Model

The fully coupled DYSMAS model showing the structure and fluid meshes is provided in Fig. 4 (Only one-half of the model is shown for clarity). In the computational models, the fluid and structural meshes are coupled through the use of

interface element definitions which define the “wetted surface” of the structure. In the current models, doubly wetted interface elements are used in which fluid (water/air) can act on both faces of the shell elements. The hydrostatic loading and subsequent collapse initiation occurs in an uncoupled preload simulation for computational efficiency. In this process, the cylindrical shell is preloaded through a dynamic relaxation procedure to the hydrostatic pressure corresponding to the collapse pressure observed in the experiment and is then subsequently squeezed upon by the radially inward moving indenter at a velocity of 60 cm/s. During this indentation phase, the velocity of the implodable volume’s wall in way of the indenter is monitored until the wall separates from the indenter, which indicates the initiation of the collapse. At this point in time, the uncoupled simulation is terminated and a file containing the relevant structural quantities (i.e. stresses, position, velocities) is written. This file is then used as the starting point for the structural component of the fully coupled DYSMAS simulation. It must be noted that for the fully coupled run, the mechanical pressures applied in the preload model are removed since the hydrostatic load is applied through coupling with the fluid domain in the coupled simulation.

4. EXPERIMENTAL RESULTS AND DISCUSSION

The pressure profiles generated from the implosion of 31.8 mm outer diameter cylindrical shells inside the confining tube are captured for the central and two longitudinal offset locations. The relation of the peak pressure, peak impulse and the end-plate cavitation duration as change in the offset is subsequently investigated. The

pressure histories are analyzed by utilizing mass and momentum conservation in order to evaluate the water velocity inside the confining tube in implosion process.

4.1 Pressure History for the Implodable Volume Placed at the Central Location

The pressure history for the implodable volume placed at the center of the confining tube is shown in Fig. 5. The collapse initiation of the volume is defined when the pressure at the nearest sensor of the implodable volume reaches 99% of the hydrostatic pressure and it is regarded as $t = 0$ ms in this study. Underwater buckling of an implodable volume generates sudden dynamic deformation in the implodable walls, causing rapid motion and decrease in dynamic pressure in the surrounding fluid. This dynamic pressure drop causes development of low pressure waves inside the confining tube. These low pressure waves are emitted from the center of the implodable volume and travel spherically outwards and later in both longitudinal directions towards the ends of the confining tube. Upon arrival at the end-plate, the rigid wall reflection of the wave results in a rapid pressure drop leading to the development of a cavitating region at the end-plate. The reflected pressure waves from the end-plates generate high pressure at the center of the tube and later evolve into high pressure hammer waves at the end-plates. The plots of pressure at both sides (left/right) of the confining tube exhibit symmetrical pressure waves for the centrally located implodable volume as observed through comparison of the left sensors (ch-1, ch-3) and the right sensors (ch-8, ch-6) in Fig. 5. Comparing the end-plate sensors (ch-1 and ch-8), it is seen that both sensors exhibit cavitation at $\sim t = 1.4$ ms and it lasts for ~ 1.5 ms. The strength of the water hammer pressure measured at both the end-plates is ~ 1.9 MPa. Interestingly, the pressure near the implodable volume (ch-B) is

maintained, although at a lower value than the collapse pressure, in the early implosion process (up to 2.5 ms).

Figure 5 Pressure history inside the confining tube for implodable volume placed at the center (a) Left half confining tube (b) right half confining tube

By utilizing all the pressure sensors located along the entire length of the confining tube, the time evolution of pressure is evaluated using spline interpolation as shown in Fig. 6. The initial plots 6(a), 6(b) and 6(c) clearly show that pressure distribution stays symmetric for upto $t = 2.4$ ms. These plots are separated for different time regimes in order to distinguish the key features of the pressure distribution evolution in time.

Figure 6 Pressure spatial distribution inside the confining tube for implodable volume placed at the center

Fig. 6(a) represents the “primary collapse phase” in which the pressure near the implodable volume drops rapidly. This phase begins at the onset of collapse and lasts until the low pressure waves released from the center reach the end-plates. The walls of the implodable undergo rapid acceleration and thus significant change in the inner volume of implodable is achieved in this phase. Subsequently, the “secondary collapse phase” initiates, causing a rapid drop of pressure and resulting cavitation at the end-plates (see Fig. 6(b)). The implodable volume continues to undergo deformation in this phase. By the end of this deformation phase, the high pressure region is formed near the implodable volume and cavitation at the end-plates is initiated. Afterwards, a net pressure gradient is created between the center and the end-plates as seen in Fig. 6(c). As at this instant the implosion process is nearly complete, this “pressure imbalance phase” can be seen as a net result of the implosion. This pressure gradient acts as an initial condition for the pressure waves oscillation in the

confining tube and it causes oscillating high pressure and low pressure waves at the end-plates. Fig. 6(d) is the “pressure reversal phase” in which the high pressure at the center releases its pressure and the end-plates’ pressure interchanges to high pressure gradually. Later, the last two phases (i.e. “pressure imbalance phase” and “pressure reversal phase”) appear periodically inside the confining tube until the pressure wave decays with time due to the energy loss in viscous flow and phase change.

4.2 Pressure History for the Implodable Volume Placed at an Offset of $5L/7$

The pressure time history and pressure distribution obtained for the implodable placed at a large longitudinal offset location, $5L/7$ away from the center are shown in Fig. 7 and Fig. 8, respectively. Note, the pressure history trend observed for the smaller offset location, $3L/7$ away from center, is similar to the case of $5L/7$ offset, hence only the case of $5L/7$ is discussed in this section for brevity of space. Similar interpretation will follow for the $3L/7$ offset results, which can be seen in Appendix A.

At the onset of implosion, the collapse of the implodable volume generates low pressure release waves in both longitudinal directions towards both the end-plates, but as a result of offset/asymmetric location, waves released on the right side of the implodable volume reach the near end-plate (ch-8 sensor) earlier than the far end-plate (ch-1 sensor). Thus, the average pressure in the near side drops significantly and near field end-plate exhibits cavitation at earlier times. The pressure evolution in this time phase is shown in Fig. 8(a). Both primary and secondary collapse phases (as mentioned earlier in previous section) occur simultaneously as the near end-plate

experiences cavitation well before the pressure waves arrive at the far end-plate. The arrival of low pressure waves causes the far end-plate to cavitate at $\sim t = 1.8$ ms with a concurrent increase in pressure at the near end-plate. Thus, the pressure gradient starts forming between the far and the near end of the confining tube, which is similar to the pressure imbalance phase described earlier. It must be noted that this pressure gradient is asymmetric about the center, in contrast to the pressure gradient seen for the case of a centrally located implodable volume. This asymmetric pressure distribution along the entire length causes strong hammer pressure at the near end-plate. Afterwards, this pressure gradient reverses its direction in the pressure reversal phase as shown in Fig. 8(d). Therefore, the far end-plate undergoes hammer pressure wave later shown in Fig. 8(e).

Figure 7 Pressure history inside the confining tube for implodable volume placed at an offset of $5L/7$

Figure 8 Pressure spatial distribution inside the confining tube for implodable volume placed at an offset of $5L/7$

The time period of hammer wave oscillation for off-centered case is much larger as compared to that for a centered case. This is due to the fact that the effective length of pressure gradient is larger ($\sim$ almost twice) for the off-centered placement than that for the center case (waves oscillate in the entire length $2L$ for off-centered placement, while for central placement, the waves only oscillate in the half length of the confining tube L). This fact can be clearly seen by comparing Fig. 6(c) and 8(c). The symmetric configuration offers generation of two symmetric pressure gradients translating into highly frequent pressure waves. On the other hand, the asymmetric configuration has a single pressure gradient formed in the entire length leading to less frequent pressure waves.

4.3 Comparison between a Centered and Highly Off-centered Placed Implodable Volume

Placement of an implodable volume at the center of a confining tube results in a symmetric configuration between the two halves of the confining tube, indicating that there will be no flow of water across the mid-length cross-sectional plane. Hence, this symmetric confining tube configuration can be separated into two effective half-length confining tubes with the central cross-section being replaced by an end-plate as shown in Fig. 9. The separation will also cause the implodable volume to split into two halves with equal volume size. Interestingly, this new configuration is asymmetric in nature in which the implodable volume is located close to the *new central end-plate* of the new confining tube. Thus, the evolution of waves in a longitudinally centered implosion within a confining tube is equivalent to two highly asymmetrically placed half-sized implodable volumes within two half-sized confining tubes. As the experiments conducted in this study at a large off-centered location ($5L/7$) already provide similar asymmetric nature, the pressure wave evolution in full length in the case of $5L/7$ offset is comparable to the half-length pressure wave evolution in the central case.

The strength of hammer wave generated also highly depends upon the symmetry. The symmetric configuration generates equal amount of hammer pressure at both ends and it is effectively generated from the half mass (and so half the kinetic energy) of water in the confining tube. As the asymmetric/off-centered configuration allows for hammer pressure wave to evolve from the larger length of confining tube

($> L/2$), comparatively much higher mass/kinetic energy generates the hammer wave.

It results into stronger hammer wave in off-centered cases.

Figure 9 Schematic of similarity between a symmetrically and highly asymmetrically placed implodable volume within a confining tube

4.4 Cavitation Duration at the End-plate

The placement of the implodable volume at the center of a confining tube allows for symmetric end-plate cavitation with equal time duration. It has been previously shown [19] that this cavitation duration at each end-plate ($T_{cavitation_symmetric}$) in symmetrically or centrally placed implodable volumes in a confining tube is approximately equal to

$$T_{cavitation_symmetric} = \frac{2L}{c_f} = 1.529 \text{ ms} \quad (1)$$

Where c_f represents the coupled longitudinal pressure wave speed in water within confining tube (1395 m/s for this study).

In the case of implodable volumes placed off-centered within the confining tube, the cavitation time duration increases at the far end-plate and decreases at the near field end-plate. If it is assumed that the cavitation occurs instantaneously at the arrival of the first low pressure wave resulting from the implosion and it exhibits cavitation until the high pressure wave from the implodable volume reaches back to the end-plate, the cavitation duration can be re-written as,

$$T_{cavitation}|_{end-plate} = \frac{2}{c_f} \left(|x_{implodable} - x_{end-plate}| \right) \quad (2)$$

Where $x_{implodable}$ is the axial coordinate of the center of the implodable volume, $x_{end-plate}$ is the axial co-ordinate of the end-plate and $T_{cavitation}|_{end-plate}$ is the duration of cavitation at the end-plate located at $x = x_{end-plate}$. It can be shown from Eq. (2), that the total cavitation duration at both the end-plates is constant irrespective of the axial location of the implodable.

$$\begin{aligned} T_{cavitation_total} &= T_{cavitation}|_{far\ end-plate} + T_{cavitation}|_{near\ end-plate} \\ &= \frac{2}{c_f} \left(|x_{implodable} - x_{far\ end-plate}| \right) + \frac{2}{c_f} \left(|x_{implodable} - x_{near\ end-plate}| \right) = 2 \left(\frac{2L}{c_f} \right) \end{aligned} \quad (3)$$

This finding shows that for a given geometry of implodable volume, it has its own constant potential for producing cavitation at the end-plates. Hence, changing the longitudinal offset of the implodable volume only changes the respective cavitation durations at each end-plate, but the sum of the individual cavitation durations is constant (for a given implodable volume). For validation of these equations, the experimental cavitation time period for each experiment is evaluated and a comparison with the solution from Eq. (2) and (3) is shown in Table 2. The measured total

cavitation durations from the experiments agree well with the Eq. (2) and (3) with less than 10% difference.

Longitudinal Offset	Cavitation Duration at Far End-plate, $T_{cavitation} _{far\ end-plate}$ (ms)		Cavitation Duration at Near End-plate, $T_{cavitation} _{near\ end-plate}$ (ms)		Total Cavitation Duration, $T_{cavitation_total}$ (ms)		
	Exp.	Theory	Exp.	Theory	Exp.	Theory	%Diff.
0	1.50	1.53	1.31	1.53	2.81	3.06	8.9%
3L/7	2.30	2.19	0.73	0.87	3.03	3.06	1.0%
5L/7	2.75	2.62	0.21	0.44	2.96	3.06	3.4%

Table 2 Comparison of experimental and theoretical cavitation durations at the end-plates. Theory represents the calculation from Eq. (2) and (3). Percentage difference is defined as $= |(Theory - Exp)/Exp| \times 100$.

4.5 Peak Pressure and Peak Impulse of the Hammer Wave at the End-plate

The peak pressure along with the impulse associated with the water hammer pressure wave defines its potential strength for damaging the confining tube structure. Both peak pressure and the peak impulse observed during the first hammer pressure wave at the near end-plate (ch-8 sensor) are shown in Table 3. The peak pressure is found to increase by $\sim 50\%$ with an increase in the longitudinal offset of the implodable volume from center to $3L/7$. Interestingly, there is minor decrease in the peak pressure with further increasing offset location to $5L/7$.

The peak impulse of the hammer wave increases significantly by introducing longitudinal offset in the location of the implodable volume. Even with a minor decrease in the peak pressure by changing the axial location of the implodable volume from $3L/7$ to $5L/7$, the peak impulse of the hammer wave exhibits a substantial

increase of 35% due to increasing time period of the hammer wave. Hence, the impulse associated in the hammer wave is directly proportional to the longitudinal offset of implodable volume.

Longitudinal Offset	Peak Pressure (MPa)	Peak Impulse (kPa-s)
0	1.88	1.87
$3L/7$	2.85 (52% ↑)	4.79 (156% ↑)
$5L/7$	2.79 (48% ↑)	6.46 (246% ↑)

Table 3 Comparison of peak hammer pressure and peak impulse observed at the end-plates for all offset locations. The values shown in brackets represent the change of the individual value with respect to zero offset case.

4.6 Comparison of Flow Field of Water inside the Confining Tube

In order to understand the flow dynamics of water inside the confining tube, the water particle velocity is calculated using fundamental mass and momentum conservation laws. These equations are solved using finite difference approach along with the spatially/temporally varying pressure data. As a result, the particle velocity at all locations within confining tube as a function of time can be obtained [19].

An evaluation of the water particle velocity field for all of the experiments performed in this study is shown in Fig. 10. The placement of implodable volume at the center leads to a symmetric particle velocity of the water. At the onset of implosion, the flow of water at any location is directed towards the center of confining tube and there exists almost zero axial velocity at the center because of the symmetrical boundary (see 0.6 ms – 1.8 ms in Fig. 10(a)). At $t = 1.5$ ms, significant velocity at the end-plates is present indicating the separation of water surface or the

initiation of cavitation at the end-plates. At $t = 1.8$ ms, the average velocity of water inside slowly disappears (see that the average size of arrow has decreased), thus suggesting that the cylinder collapse has completed.

Figure 10 Uni-axial water particle velocity generated inside the confining tube for the case of (a) centrally placed (b) $3L/7$ away from center (c) $5L/7$ away from center. The size of the arrow shown in this figure is proportional to the magnitude of longitudinal particle velocity of water at that location.

Fig. 10(b) and 10(c) represent the water particle velocity field for two asymmetric locations of the implodable volumes within the confining tube. As seen earlier in Fig. 10(a), the onset of the implosion generates similar symmetric flow of water towards the implodable volume, but the asymmetric location causes water particles at the near end-plate to move and separate from the end-plate much earlier than at the far end-plate. It is interesting to note that by time $t = 1.5$ ms in Fig. 10(b), there exists net flow/velocity towards the near end-plate even across the implodable volume. As the majority of the water particle motion is towards the right direction, it causes over compression of water at the near end-plate exhibiting zero particle velocity. This unidirectional flow of water generated as a result of implosion is the primary cause of high pressure hammer wave.

5.1 Computational Model Correlation

The pressure time histories recorded during the experiments are the primary means of the model correlation for this study. The comparison between the experimental and computational pressure histories at the center (ch-B sensor) and at the near field end-plate (ch-8 sensor) of the confining tube are presented in Fig. 11. From these figures it is seen that there is good agreement between the simulation and experimental results. The model is able to accurately capture the rate at which the pressure drops during the collapse of the implodable volume, and the magnitude of the corresponding pressure drop. The simulations are further shown to accurately capture the arrival of the positive pressure wave resulting from the collapse at the endcap. Similar levels of correlation are observed for the remainder of the sensors along the length of the confining tube.

Figure 11 Pressure history correlation between experiments and simulations for the case of (a) centrally placed (b) $3L/7$ away from center (c) $5L/7$ away from center.

Table 4 shows the correlation of the peak pressure and the impulse present in the hammer wave at the near end-plate. The comparison suggests that though there exists small difference between the experimental and numerical peak pressures, the numerical impulse of the hammer wave is found to be in good agreement and shows similar trend as seen in the experiments. Furthermore, the models are able to predict the initiation time of the cavitation at the end-plate (see Fig. 11) as well as the overall time duration of the cavitation phase shown in Table 4. Based on these levels of correlation between the experiments and corresponding simulations, the models are

deemed to be acceptable for reliable use in the investigation of additional longitudinal offsets that are not explicitly studied experimentally.

Longitudinal Offset	Peak Pressure at the Near End-plate (MPa)		Impulse at the Near End-plate (kPa-s)		Total Cavitation Duration, $T_{cavitation_total}$ (ms)	
	Exp.	Simulation	Exp.	Simulation	Exp.	Simulation
0	1.88	1.00	1.87	1.43	2.81	2.88
$3L/7$	2.85	3.86	4.79	4.10	3.03	3.03
$5L/7$	2.79	2.83	6.46	6.79	2.96	2.98

Table 4 Comparison of peak pressure, impulse at the near end-plate as well as the total cavitation duration between experiments and simulations

5.2 Near-field End-plate Pressure History Effects

The effect of longitudinal offset on the pressure history experienced at the end-plate closest to the implodable volume has been numerically studied to determine the trend in the impulse as a function of offset. Figure 12(a) shows the pressure time history at the near end-plate for offset values from zero to $5L/7$ in $L/7$ increments. For clarity, the time histories have been aligned in time by utilizing the initiation of the pressure drop as time zero. In the actual simulations, the initiation of this pressure drop is delayed as a function of longitudinal offset (smaller offset pressure drop arrives later in time). From this figure, it is seen that the $3L/7$ and $4L/7$ offsets result in the largest peak pressure experienced by the near field end-plate. Furthermore, the hammer wave's impulse at near end-plate as a function of offset location is plotted in Fig. 12(b). This figure indicates that as the implodable volume is moved from the center of the confining tube towards the end-plate, there is a monotonic increase in the

impulse of the hammer wave. This increase is almost linear with increasing offset location.

Figure 12 (a) Pressure history at the near end-plate from simulations for varying offset locations (b) Pressure-impulse observed at the end-plate as a function of offset location

5.3 Implosion Collapse Rate

The rate of implodable collapse as a function of longitudinal offset is shown in Fig. 13 through the computational models. The collapse wall velocity at the center point and the change in the internal volume of implodable ($\Delta V_{implodable}$) is utilized as the measure of the rate of collapse. From the Fig. 13(a), it is seen that the longitudinal offset of the implodable volume within the confining tube has a negligible effect on the center point velocity of the implodable volume. For each offset location, the node at the top of the implodable volume achieves a maximum inward velocity of 25 m/s. This indicates the initial rate of collapse prior to the wall contact is similar for all offset locations (see plot of $\Delta V_{implodable}$ in Fig. 13(b) upto 1.5 ms).

After the initiation of wall contact, it is seen that the rate of change of $\Delta V_{implodable}$ decreases with the increasing offset as shown in Fig. 13(b). Although this

suggests the axial buckle propagation after the contact is slower for higher offset cases, the final collapse shape is consistent across all the offset locations indicated by same final value of $\Delta V_{implodable}$ at later times.

Figure 13 (a) Center-point velocity of the implodable volume for all offset locations
(b) Change in the internal volume of the implodable for all offset locations

6. CONCLUSIONS

An experimental study, with corresponding computational simulations is conducted to understand the implosion mechanics of offset implodable volumes inside a confining environment. The key findings of this study are as follows:

- (1) The longitudinal off-centered placement of the implodable volume has a significant effect on the implosion mechanics inside the confining tube. When placed symmetrically at the center, the generation of pressure waves is symmetric and the both the end-plates experience hammer wave simultaneously. In the case of off-centered location, the net velocity of water shifts towards the implodable volume and it generates higher pressure hammer wave at the end-plate near to the implodable volume.

- (2) The duration of cavitation at individual end-plates of the confining tube shifts with longitudinal offset of the implodable volume. Although, the total duration of cavitation at the end-plates is nearly constant, the individual cavitation duration at each end-plate increases with its increasing distance from the implodable volume.
- (3) The impulse of the hammer wave increases by 3 to 4 times, as the implodable volume is moved away from center of the confining tube.
- (4) Although there is significant effect of the longitudinal offset on the pressure time history and peak hammer pressure observed at the end-plates, the initial collapse velocity history of the implodable volume prior to wall contact stays independent of its longitudinal location. The collapse rate during axial buckle propagation is observed to decrease with increasing offset.

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APPENDIX A

Figure A1 Pressure history inside the confining tube for implodable volume placed at an offset of $3L/7$

Figure A2 Pressure spatial distribution inside the confining tube for implodable volume placed at an offset of $3L/7$

CHAPTER 7

INFLUENCE OF TRAPPED AIR ON THE HYDROSTATIC IMPLOSION OF TUBE IN A TUBE

by

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Abstract

Trapping of air bubbles in fluid filled pressure vessels enables for high compressible energy storage upon pressurization, and it can significantly alter the implosion mechanics within confining environments. In this paper, a comprehensive series of experiments were conducted to investigate the detrimental effects of trapped air bubble inside the confining tube on the implosion process. Two different sizes of air bubbles were introduced prior to pressurization to study the effect of air bubble, such that the final compressed air bubble size at the initiation pressure is 15% and 30% of the inner volume of the implodable. Experiments were conducted with two geometries of the implodable volume, 31.8 mm and 50.8 mm outer diameter, with approximately similar collapse pressure. High-speed photography coupled with 3-D digital image correlation was utilized to capture the real-time deformation of the implodable for all the experiments. Experiments revealed that the presence of air bubble inside the confining tube significantly affects the compliance of the system leading to slower waves propagating inside the confining tube. The cavitation occurring at the end-plates is inhibited by introducing air bubble, leading to suppression of cavitation shock-wave damage at the end-plates. DIC full field deformation and velocity results show that the confined nature of the environment significantly alters the velocity of the implodable volume during collapse. The implodable volume accelerates in the initial collapse phase, but reaches its peak velocity well before the wall contact initiation or the maximum radial deformation due to large near-field pressure drop due to collapse of implodable volume. A deceleration in the implodable volume is also observed suggesting the presence of strong fluid-

structure interaction between water and implodable. An addition of air bubble inside the confining tube resulted in a further increase in the collapse velocity of implodable volume and this effect is observed to further increment with increasing air bubble sizes.

Keywords

Implosion, Implodable Volume, Collapse, Pressure Waves, Trapped Air Bubble, Compressible Air Energy, Fluid Structure Interaction, Confined Environment, High Speed Photography, 3D Digital Image Correlation

1. Introduction

The effect of air bubble's presence on the mechanics of implosion inside tubular confining space is experimentally investigated. High-speed photography coupled with 3-D digital image correlation is incorporated to capture the real-time deformation of the implodable volume during the confined implosion process. Experiments are conducted with 31.8 mm and 50.8 mm outer diameter, cylindrical aluminum 6061-T6 implodable volumes. The implodable volumes are placed at the longitudinal center concentrically to the confining tube and two sizes of air bubbles are introduced inside the confining tube to understand the changes in the velocity of collapse and the pressure history.

Extensive use of cylindrical shells in the underwater and aerospace applications structures has led to increasing interest in the research in this area [1, 2]. In the design of underwater structures such as deep ocean submersibles, submarines,

underwater remote operated vehicles, underwater pipelines etc., cylindrical shells face extreme external hydrostatic pressures at deep sea depths. If the shell contains relatively lower pressure (or atmospheric pressure) gas inside, the shell can become structurally unstable at certain critical hydrostatic pressure depending on the geometry and will collapse/implode onto itself. The implosion of the structure is highly violent and it results in a rapid release of energy in the form of strong shock waves, high velocity fluid motion, and sound [3].

Understanding the mechanics of implosion has been a topic of great interest for several researchers. Earlier studies have conducted experiments with glass sphere and characterized the nature of the implosion pressure pulse [4–9]. These studies also compared the experimental data with Raleigh’s gas bubble collapse theory [10] in order to predict the time period/peak pressure of the implosion pressure pulses. Recently, numerous experimental and numerical studies have also been reported on the mechanics of implosion in free-field environments [1, 2, 11–14]. Turner measured the near-field pressure history during the implosion of glass spheres and conducted corresponding numerical modeling using a coupled fluid-structure interaction code [11]. Turner concluded that the failure time history of the structure has a significant influence on an implosion pressure pulse. Later, both Turner and Ambrico, and Farhat et al. studied the implosion of aluminum cylindrical tubes experimentally and numerically [1, 2]. Turner and Ambrico identified the key features of a typical mode-2 implosion process by correlating the important signatures of the pressure pulse with the structural deformation [1]. Farhat et al. extended this work by comparing both mode-4 and mode-2 implosion, and showed that the higher mode of collapse generates

a higher pressure peak but of smaller duration compared to that obtained in a lower mode collapse [2]. Recently, Gupta et al. utilized a high speed Digital Image Correlation technique to capture the real-time deformation of the implodable volumes, and showed that the time period and impulse of the positive pressure wave directly correlates to the time taken for the collapse to propagate along the length of the implodable [15].

The recent studies conducted by the authors have shown that the mechanics of implosion in a confining environment is entirely different from an implosion occurring in a free-field environment. In a free-field environment, the net hydrostatic pressure in the surrounding fluid is maintained during the implosion process, while in the case of implosion occurring within a confining space, the source of hydrostatic potential energy to progress the implosion is limited. Recently, the authors performed couple of comprehensive investigations on the implosion of cylindrical shells within a tubular confining environment [16, 17]. The result of these studies indicated that the limited hydrostatic potential energy present in water significantly affects the implosion process. The rate and extent of collapse dramatically reduces due to sudden decrease of potential energy inside the confining tube. The findings of these investigations also indicated that the presence of an air bubble inside the confining tube can aid in maintaining the hydrostatic pressure and therefore can increase the available hydrostatic potential energy, which may results into higher degree of collapse in the implodable volume. Hence, the current study investigates the effect of trapped air bubble size on the implosion mechanics inside the confining tube. In particular, the focus is to understand the changes in the structural deformation behavior with

increasing air bubble size. This study also extends author's previous work with the incorporation of high-speed digital image correlation technique in order to accurately measure the real-time dynamic deformations. High-speed DIC results suggest that the introduction of air bubble in sufficiently large implodable volume increases the collapse velocity as well as the amount of collapse during implosion process. It is also seen that the relatively small size implodable volume doesn't exhibit any significant change in the early deformation process prior to wall contact, but the extent of collapse progression still increases with increasing air bubble size. Study also shows that the pressure wave evolution inside the confining tube is largely affected due to the reduced compliance of the inner fluid volume.

2. Experimental setup

2.1 Confining tube facility and implodable volume

A schematic of the experimental setup for the tubular confined space implosion facility is shown in Fig. 1. The confining tube has an inner diameter of 177.8 mm (7 in) with 19 mm (0.75 in) wall thickness and is made out of seamless low-carbon steel (SA106-B) pipe. The overall internal length ($2L$) of the confining tube is 2.29 m (90 in). The confining tube is fabricated in a modular design. It contains two sections of 914 mm (36 in) length separated by a 457 mm (18 inch) long window section for high-speed photography of the implosion process (see Fig. 1). This central section has a 63.5 mm (2.5 in) thick flat acrylic viewing window with a viewable area of approximately 216 mm x 102 mm (8.5 in x 4.0 in). A cross-sectional view of the

central section is shown in the inset of Fig. 1. It must be noted that insertion of the window results into $\sim 15\%$ difference between the front/back cross-sectional areas.

Figure 1 Schematic of confined tube implosion facility

The implodable volumes used in this study are comprised of commercially available aluminum 6061-T6 extruded seamless tubing. A schematic of the mounting fixture for holding the implodable is shown in Fig. 2. Table 1 provides a summary of the experiments conducted and the details of the specimen used in this study. Two geometries of the implodable volumes are studied: (1) 31.8 mm outer diameter (referred as Geo A later in the article), and (2) 50.8 mm outer diameter (Geo B). The implodable volumes are sealed on both ends using solid aluminum end-caps, which utilize circumferential o-rings for sealing the specimen for underwater experiments. This tube and end-cap assembly is mounted inside the confining tube using support collets, which ensure concentric placement of the implodable in the confining tube

(see Fig. 2). The construction of the three spoke collet is chosen so as to provide minimal interference to the axial pressure waves and water flow within the confining tube during the implosion event. The confining tube is filled and pressurized with water using a hydrostatic test pump. The confining tube is inclined at $\sim 2^\circ$ at one end manually in order to remove the trapped air bubble in the confining tube to the maximum extent possible for no air bubble experiments. High frequency dynamic pressure transducers (PCB-113B22) are installed in the wall of the vessel at various axial locations such that the transducer sensor element is flush with the inner surface (see Fig. 3 for exact locations).

Experiment Label	Pressure at Collapse Initiation (P_c)	Outer diameter (d)	Wall Thickness (t)	Internal Volume of Specimen ($V_{implodable}$)	Volume of Trapped Air Bubble at Collapse Pressure
GeoA0	3.10 MPa (450 psi)	31.8 mm	0.89 mm	215 ml	None
GeoA30		(1.25 in)	(0.035 in)		64.5 ml (30% $V_{implodable}$)
GeoB0		50.8 mm	1.24 mm	559 ml	None
GeoB15		(2.00 in)	(0.049 in)		87.4 ml (15% $V_{implodable}$)
GeoB30					174.8 ml (30% $V_{implodable}$)

Table 1 Layout of the experiments conducted in this study

Figure 2 Schematic of the implodable volume with end-caps and support collets

A pair of Photron SA1 high speed cameras, as shown in Fig. 1, is placed in front of the viewing window at a distance of ~ 1.3 m with a stereo angle of 13° . The high speed images are captured at a frame rate of 40,000 fps for Geo A and at 20,000 fps for Geo B.

A small volume of air bubble is introduced prior to complete closure of the confining tube to study the effect of the trapped air bubble. The size of the air bubble introduced is such that it is equal to a certain fraction of the inner volume of the implodable ($V_{implodable}$). Two air bubble volumes are chosen: (1) 15% $V_{implodable}$ and, (2) 30% $V_{implodable}$. Due to the buoyancy of the air bubble, it stays on the top side inside the confining tube, as represented in Fig. 3.

Figure 3 Schematic of the implodable volume with the end-caps and the support collets. The air bubble is shown in the top side of the confining tube. The air bubble has been oversized in schematic for visibility.

Author's previous study has shown that the initial pressure at the initiation of the collapse largely affects the initial available hydrostatic energy for driving the implosion process [16]. But the natural buckling pressure of the tubes can significantly change between the experiments due to the presence of imperfections (ovality and wall eccentricity) in the specimens. Moreover, the pressurization of the confining tube using a hydrostatic pump leads to post implosion pressure loading on the specimen,

which can result into further collapse of the implodable volume. Therefore, in order to control the collapse pressure and to explicitly capture the post mortem shape of the implodable volume, all the experiments conducted in this study are mechanically initiated at a constant pressure of ~ 3.10 MPa, slightly below the critical collapse pressure of both the geometries. The procedure of mechanical initiation described by Turner and Ambrico is followed for the experiments [1]. A hydraulic cylinder with a piston (which acts as an ‘indenter’) is installed behind the implodable volume at the mid-length of the confining tube. After reaching the desired initiation pressure of 3.10 MPa, the confining tube is completely isolated from the pressurization unit. Subsequently, the piston of hydraulic cylinder is slowly pushed radially inwards towards the implodable volume. It causes squeezing the central diameter of the implodable volume and it makes the implodable volume structurally unstable eventually initiating the implosion process. As the indenter moves in the normal direction of the window, the center viewing area of the implodable volume becomes the minor diameter during mode-2 shape formation. This orientation of piston leads to maximum viewable area for high speed photography producing better full field DIC results.

2.2 3-D digital image correlation calibration setup

Application of 3-D digital image correlation technique for submerged applications requires rigorous two-step calibration procedure, as outlined in [18]. Although this procedure has been shown to accurately measure both the three dimensional position and the displacements/strains of the submerged objects, the procedure of calibration and correction is complex, and it requires extensive post-

processing of the data. Recently, It has also been shown that calibration of extrinsic and intrinsic parameters by using a submerged calibration target can result into high accuracy in both in-plane and out-of-plane displacement measurements using 3-D DIC [15]. As the primary motive of using 3-D DIC in this study is to measure out-of-plane displacements/velocity in the confined implosion experiments, the authors have used the similar submerged calibration procedure as shown in [15].

To evaluate the accuracy of measured DIC displacements in a submerged situation, calibration experiments are conducted in a custom designed tank (shown in Fig. 4), which replicates the design of the middle section of the confining tube. This setup replicates the optical effects of submersion in the middle section of the confining tube using a more accessible smaller tank. The setup is approximately 400 mm (16 in) wide and 400 mm (16 in) long with a height of ~ 200 mm (8 in). The front viewing window utilized in the custom designed tank setup is identical to that of the middle section's window in confining tube. A 250 mm x 100 mm (10 in x 4 in) flat aluminum specimen is placed inside the tank on a precision translation stage, which can provide translations with 0.01 mm accuracy. The aluminum specimen is placed at ~ 70 mm (2.75 in) deep in water measured from the back of the viewing window, which represents the typical location of the implodable volume in the middle section of the confining tube. A random high contrast black/white speckle pattern is painted on the front surface of the specimen for DIC measurements.

The SA-1 high-speed cameras, with the same front-end optics, are used to capture images of the translated specimen during calibration experiments. The resolution of the camera image is 1024×1024 pixels, corresponding to an

approximate magnification factor of 4.02 pixels/mm. Calibration of intrinsic and extrinsic parameters is performed using a submerged calibration grid (12 dots x 9 dots, 7 mm interspacing) provided by Correlated Solutions, Inc.[19]. Using the translation stage, the specimen is translated in 1 mm increments in both in-plane and out-of-plane directions and the corresponding images are captured. Displacement of the specimen at each translation increment is estimated with Vic-3D 2012 software using 25×25 pixels subsets and a step size of 7 [19].

Figure 4 Custom designed tank setup for underwater DIC calibration experiments

3. Experimental results and discussion

3.1 Underwater 3-D DIC calibration results

The out-of-plane 3D DIC calibration results for submerged objects are shown in Fig. 5. Both displacements and strains (von-Mises) are calculated from the 3D DIC

measurements. To quantify the accuracy of the measured displacement and strains, the average surface displacement and standard deviation over the whole area for each given displacement is calculated. Note that the as rigid translation is not supposed to produce any strains on the specimen; the measured von-Mises strain from DIC (pseudo strain) is an artifact due to the change in refractive index due to water and the viewing window in these experiments. The range of pseudo strain is calculated across the area such that 95% of the values lie in the range (which equals to two standard deviation interval from theory of Gaussian distribution). A plot of the actual out-of-plane displacement and the measured average DIC displacement of the specimen is shown in Fig. 5(a). The measured average DIC displacements are in good agreement with the true displacements and the percentage error between true and measured out-of-plane displacement is less than 4%. The maximum range of pseudo strains is found upto 0.08% (see Fig. 5(b)), which is significantly higher than the strain accuracy of the 3D DIC technique ($\sim 0.02\%$ from [20]) in air.

The in-plane 3D DIC calibration results for submerged objects are shown in Fig. 5(c). A comparison of actual in-plane displacement and measured average DIC displacement as shown in Fig. 5(c) indicates that the two are in very good agreement. The magnitude of error is $< 1\%$ for all in-plane displacements. The range of pseudo strains is found to be upto 0.11% (see Fig. 5(d)). As the measurement of out-of-plane displacements is the primary interest in the implosion experiments, it can be concluded that both in-plane and out-of-plane displacements can be measured with adequate accuracy using this modified DIC calibration in the case of submerged object viewed through a single flat window. The extraction of camera's intrinsic and extrinsic

parameters using submerged calibration grid can successfully account for the change in refractive index due to water and acrylic viewing window.

Figure 5 Underwater calibration results (a) out-of-plane displacement results (b) out-of-plane pseudo strains (c) in-plane displacement results (d) in-plane pseudo strains

After the successful establishment of the modified 3D DIC calibration procedure for submerged objected viewed through a single flat window, high speed cameras are moved to the confining tube implosion facility. Cameras are placed at approximately same distance from the viewing window as in the calibration experiments. To disregard any differences present between the calibration setup and the window section of the confining tube, cameras are recalibrated using the same submerged calibration grid placed inside the middle section. The calibration grid is moved in the region encompassed by the largest implodable volume used in this study (Geo B). Once the calibration process is completed, the accuracy of 3D DIC displacement measurements in the confining tube implosion facility is re-estimated by

quantifying the radius of both Geo A and Geo B implodable volumes using 3D-DIC technique. After reconstructing the cylindrical shape of the implodable volume using Vic-3D 2012 software, the local radius of curvature of the specimen surface is estimated (see Fig. 6) using data from the central region of the specimen. The results for both the geometries are summarized in Table 2. It can be seen that the measured average radius of the specimens using 3D DIC matches very well with the true radius for both of the geometries with a maximum error of 2%. Thus, it can be established that both the shape and 3-D deformation of submerged objects can be measured accurately using the modified calibration process described in Sec. 2.2.

Figure 6 Measurement of the radius of the implodable volume using 3-D DIC. (a) Geo A (31.8 mm OD) (b) Geo B (50.8 mm OD)

Geometry True Radius		Measured Average DIC Radius (95% Confidence)	Percentage Error
GeoA	15.88 mm	15.68 mm $\pm$ 0.20 mm	1.26%
GeoB	25.40 mm	25.09 mm $\pm$ 0.28 mm	1.22%

Table 2 Comparison of measured average radius from DIC with the true radius of the implodable volumes

3.2 Real time deformation and pressure history

The real time out-of-plane deformation of the implodable volume is captured using modified high-speed 3-D digital image correlation as mentioned in Sec. 2.2. Pressure history is synchronized with the high-speed photography to correlate the structural deformations with the pressure inside the confining tube. The deformation mechanics during a confined environment captured using 3-D DIC is discussed first and later the effect of air-bubble has been explained.

3.2.1 Typical deformation mechanics in confining environments without air-bubble

3.2.1.1 “**Complete**” collapse of small sized implodable volumes (GeoA0)

A typical out-of-plane deformation and velocity contour plot of small sized implodable volumes (GeoA0) undergoing confined implosion is shown in Fig. 7. The relative small size of GeoA implodable volume results into a complete mode-2 collapse and the real-time deformation is shown in Fig. 7. The complete implosion process in confined implosion consists primarily of four phases: (a) initially accelerating recession of wall reaching the peak velocity, (b) the deceleration of the walls prior to wall contact initiation, (c) wall contact initiation and progression of wall contact along the central diameter, and (d) propagation of buckle towards the end-caps of the implodable volume.

Figure 7 Real time deformation and velocity contours for a typical GeoA confined tube implosion

Note that the time $t = 0$ ms represents the time when the pressure in the near-field of the implodable volume drops to 99% of the hydrostatic pressure. Hence, time $t = 0$ ms represents the beginning of dynamic implosion process. Fig. 7 outlines the general deformation mechanics of an implodable volume undergoing an implosion process within a confined tube. Similar to seen in [15], a significant pre-deformation (~ 1.76 mm, 11% of the radius) is present in the GeoA implodable volumes at the beginning of dynamic implosion. As the implodable volumes are mechanically initiated using an indenter at a pressure lower than the critical buckling pressure of the

geometry, the ovalization seen prior to dynamic implosion is expected due to the squeezing caused by the indenter. The ovalization essentially reduces the critical buckling pressure of the geometry with change in geometrical shape. After reaching the critical ovality, the critical buckling pressure of the ovalized shape reaches the hydrostatic pre-pressure inside the confining tube, causing specimen to undergo dynamic implosion process.

Inspecting the velocity contours in Fig. 7, it can be seen that the velocity prior to time $t = 0$ ms is relatively small (< 2 m/s) causing no change in the dynamic pressure in the near-field. After time $t = 0$ ms, walls of the implodable volume accelerate quickly upto $t = 0.8$ ms and reaches the maximum wall velocity of ~ 22 m/s. In most of the free-field underwater implosions, the maximum wall velocity at the center point is achieved marginally before the initiation of wall contact [15]. But in the case of confined implosion as shown in here, the implodable volume only deforms ~ 9.2 mm at the center (58% of the radius) at the time of maximum wall velocity. It signifies the strong fluid-structure interaction between the implodable volume's deformation and the resulting pressure in the near-field water. The increasing deformation of the implodable volume generates enough near-field pressure drop such that the pressure is not sufficient to accelerate the implodable volume beyond $t = 0.8$ ms and the walls starts slowly decelerating after time $t = 0.8$ ms (as shown in $t = 1.0$ ms to 1.1 ms). This deceleration causes the center-point velocity to drop to ~ 17 m/s at time $t = 1.1$ ms and subsequently, the wall contact initiates at $t = 1.15$ ms. The measured out-of-plane DIC deformation at the wall contact is equal to 15.7 mm, which

is close to inner radius of Geo A (15.0 mm) confirming a symmetric mode-2 deformation.

As soon as the wall contact initiates, the velocity of water following the implodable volume abruptly vanishes resulting to its large over-compression against the implodable volume. The initiation of wall contact begins as a point contact at the center point, which generates a short duration pressure spike in the near-field pressure (discussed later in sections). This contact grows in both diametrical direction and longitudinal direction seen at $t = 1.2$ ms. It is interesting to note that during the propagation of contact along the longitudinal direction, two symmetric high velocity region (see image at time $t = 1.2$ ms) appears in both directions similar to seen in [15]. By time $t = 1.3$ ms, the wall contact along the central diameter is achieved and the high velocity region disappears. In free-field situations, these high velocity regions seem to steadily moving with time and maintain its shape until it reaches the end-cap. But this disappearance in the case of confined implosion seems to be the effect of lower pressure in the near-field due to confined environment. The steady propagation of contact front requires high driving pressure, but the decreasing pressure in the confining tube drastically reduces the ability to drive the contact front. By time $t = 1.4$ ms, the contact front becomes nearly planar. Interestingly, the velocity of contact is very small between 1.4 ms and 1.7 ms and it again start moving at a faster rate after 1.9 ms.

3.2.1.2 “**Partial**” collapse of large sized implodable volumes (GeoB0)

A typical out-of-plane deformation and velocity contours for GeoB implodable volume with no air bubble confined implosion is shown in Fig. 8. The relative large

size of GeoB implodable volume with respect to the confining tube results into a partial collapse (see [16] for details). The mechanics of initial wall recession deformation in the partial collapse is similar to as discussed in previous section. It can be seen in Fig. 8 that wall accelerate upto time $t = 1.9$ ms and reaches approximately ~ 11.6 mm deformation at the center-point (45% of the radius).

The comparison between velocity contour at time $t = 2.2$ ms (after maximum velocity) and time $t = 1.6$ ms (prior to maximum velocity) suggests that reaching the peak velocity at time $t = 1.9$ ms, contours localizes at the center region while the velocity at points longitudinally away vanishes to nearly zero. The implodable volume reaches ~ 16 mm (63% of the radius) center point deformation at $t = 2.35$ ms and the velocity in the center point drops to zero afterwards.

Although, GeoB doesn't exhibit wall contact due to the significant drop in pressure in the confining tube, it is interestingly seen that the DIC velocity contours between time $t = 2.35$ ms and 2.6 ms show the evidence of “longitudinal buckle propagation” even in the *absence of a physical wall contact*. Similar high velocity regions that are generally formed ahead after the contact regions are seen to be propagating longitudinally towards the end-caps of the implodable volume. It appears that the central region of the implodable volume gains significant momentum during the initial wall recession phase. After the driving pressure around the implodable is sufficiently lower than needed to progress the deformation, the momentum of the wall itself progress the implosion process until it dissipates all its kinetic energy. Therefore, the two high velocity regions originate at the center and move towards in the end-caps in longitudinal direction. This artificial formation of high velocity regions also suggest

that the primary cause of formation of these regions is due to the high deceleration present in the central region. As a complete collapse leads to wall contact, the deceleration is caused by the physical wall contact of the two oppositely moving walls. But in the case for Geo B confined implosion, the deceleration of the walls is caused by larger pressure drop near the center region leading to artificial longitudinal buckle propagation. It can also be observed that the subsequent motion of these regions dissipate a lot of energy as plastic energy absorbed in the implodable, these regions also lose their velocity magnitude while moving away as seen in Fig. 8.

Figure 8 Real time deformation and velocity contours for a typical GeoB confined tube implosion

3.2.2 Effect of trapped air bubble on the pressure history and deformation mechanics

The real time out-of-plane deformation of the implodable volume captured using modified high-speed 3-D digital image correlation for different air bubble sized will be discussed in this section. It must be noted that though the implodable volume is placed at the center of the confining tube longitudinally, the pressure history observed in the each side of confining tube doesn't show symmetry. Authors believe that due to relatively small size of air bubble at the collapse initiation pressure, the surface tension along with the curved surface of confining tube doesn't allow for a uniform air bubble on the topmost longitudinal line. Hence the air bubble tends to split in several small-sized air bubbles residing at discrete longitudinal locations inside the confining tube (see schematic in Fig. 9). This random splitting of air bubble results into unsymmetrical distribution of the air bubble in longitudinal direction, leading to different pressure histories seen at two end-plates of the confining tube⁵. Therefore, for comparison purposes, the pressure history near the implodable volume and the mean hydrostatic pressure (averaged over the length) is discussed for understanding the effect of air bubble.

Figure 9 Random distribution of air bubble after pressurization

⁵ Other possible reasons for this unsymmetrical splitting of air bubble are: (a) surface texture/undulations inside the confining tube, (b) minor misalignment between the modular sections.

3.2.2.1 *Geo A0 vs Geo A30*

The pressure history observed near the implodable volume (ch-B sensor) along with mean hydrostatic pressure (spatially averaged pressure inside the confining tube) is shown in Fig. 10. The pressure history for Geo A0 and Geo A30 exhibits similar characteristic behavior as seen in author's previous studies [16, 17]. Underwater buckling of the implodable volume generates sudden dynamic deformation in the implodable walls, causing rapid motion and decrease in dynamic pressure in the surrounding fluid. This dynamic pressure drop causes development of low pressure waves inside the confining tube. These low pressure waves are emitted from the center of the implodable volume and travel spherically outwards and later in both longitudinal directions towards the ends of the confining tube. These low pressure waves gradually decrease the mean hydrostatic pressure inside the confining tube in Geo A0, but the expansion of air bubble in the case of Geo A30 results into smaller change in dynamic pressure. The effect of air bubble expansion starts to appear at time $t = 1.0$ ms, which leads to significant recovery in the pressure near the implodable volume. The air bubble expansion results into a higher pressure hammer wave, $80\% P_c$, observed at the implodable volume. It also increases the residual pressure inside the confining tube after the collapse of implodable volume. Both Fig. 10(a) and (b) indicate that approximately $40\% P_c$ residual pressure is present in the confining tube for Geo A30, in comparison to $15\% P_c$ for Geo A0.

The full field deformation contours observed using 3D DIC reveals that the overall deformation behavior during implosion is similar between Geo A0 and Geo A30 (as earlier shown in Fig. 7). To explicitly evaluate the effect of air bubble, the

out-of-plane deformation and the velocity are plotted at the center point of the implodable volume to understand the initial deformation process prior to wall contact. Also a point at 76 mm longitudinal offset is also investigated to identify differences due to air bubble during the buckle propagation. It can be seen from these plots, as shown in Fig. 11(a) and (b), that the both the center point and the 76 mm offset point reach wall contact exhibited by (I) approximately 15-16 mm deformation, and (II) high deceleration shown by an abrupt change in velocity due to wall contact. As 76 mm offset point shows contact, it also confirms that buckle must have propagated at least to half the length of implodable volume. The velocity history seen at the center-point is exactly same indicating that the structural deformations are unaffected by the presence of air bubble prior to wall contact, which occurs at ~ 1.10 ms. This observation is consistent with the pressure history's signatures showing similar trend upto approximately same time (~ 1.00 ms). Both Geo A0 and Geo A30 achieve a peak velocity of 22 m/s at $t = 0.8$ ms followed by a deceleration phase for ~ 0.3 ms due to surrounding lower pressure. Later at time $t \sim 1.10$ ms, both exhibit wall contact.

Figure 10 (a) Pressure history near implodable volume and (b) the mean hydrostatic pressure history comparison between Geo A0 and Geo A30

Interestingly, there are some differences observed in the velocity history at 76 mm offset point. The velocity at this point increases during initial wall recession linearly upto $t \sim 0.8$ ms, and reaches a velocity of 8 m/s for Geo A0 and 9 m/s for Geo A30. It starts slowing down to zero velocity during the center point's deceleration phase upto the center point's wall contact. This deceleration is similar to seen in [15], which signifies the localization of mode-2 deformation in the central region. This point again starts accelerating with the propagation of buckle in longitudinal direction. The maximum velocity reached during buckle propagation is 18 m/s for Geo A0 and 20 m/s for Geo A30. Thus, the presence of air bubble increases the magnitude of peak velocities by $\sim 10\%$ during both the initial wall recession and the buckle propagation.

Figure 11 (a) Center point out-of-plane deformation/velocity history (b) 76mm offset point out-of-plane deformation/velocity history. Hollow symbols represent deformation and solid symbols represent velocity.

3.2.2.1 Geo B0 vs Geo B15 vs B30

The out-of-plane velocity contours along with center point deformation and velocity history for all Geo B experiments are shown in Fig. 12 and 13 respectively. It can be easily seen that the evolution of collapse in all the cases is entirely different.

The effect of air bubble size is seen to affecting the velocity history from the beginning of the collapse. Geo B30 with largest air bubble shows highest acceleration leading to a maximum velocity of 18 m/s by $t = 1.40$ ms, while Geo B15 and Geo B0 are still accelerating and have a velocity of 13 m/s and 8 m/s respectively at this time. Geo B15 reaches a peak velocity of 16.5 m/s at 1.65 ms, and Geo B0 reaches a peak velocity of 14 m/s at 1.9 ms. Similar to shown earlier in Sec. 3.2.1, all geometries exhibit deceleration following the peak velocity.

Interestingly, both Geo B0 and Geo B15 exhibit the generation of virtual/artificial contact front (represented by local high velocity) during the deceleration phase which can be seen at $t = 2.35$, 2.45 and 2.65 ms in Fig. 12. These virtual contact fronts are more clearly visible in Geo B15 in comparison to Geo B0. It is also surprising that these local high velocity regions don't appear to propagate further in the longitudinal direction which can be thought of as a "stationary virtual buckle". This "stationary nature" of high velocity regions seems to be due to relative high pressure drop in the confined implosion experiment. By around this time $t = 2.35$ ms, Geo B30 reaches a wall contact at the center point indicated an abrupt change in velocity from 8.5 m/s to 0 m/s .

By time $t = 2.85$ ms, it can be seen that the structural velocity in Geo B0 starts vanishing showing that it has reached its maximum deformation of ~ 18.5 mm (see Fig. 13 (a)) and no further collapse occurs after $t = 3.15$ ms in Geo B0. In the case of Geo B15, the presence of air bubble leads to larger deformation even during deceleration phase and a wall contact is observed at time $t = 2.85$ ms. The contact region slowly grows in both direction leading to formation of "actual contact front".

Thus, Geo B15 shows unique nature of both virtual and actual contact fronts in a confined implosion experiment. For the case of Geo B30, a similar contact growth is seen except that the velocity ahead of the actual contact front is much larger as compared to Geo B15 (see Fig. 12 at $t = 2.45 - 3.15$ ms).

Figure 12 DIC Out-of-plane velocity contours for (a) Geo B0, (b) Geo B15, and (c) Geo B30

Figure 13 (a) Center point deformation history (b) center point velocity history

Overall, it can be concluded that the presence of air bubble leads to larger structural deformations and velocities in the case of confined implosion. The structural deformations at relatively larger air bubble size in confined implosion may become comparable or similar to a free field implosion experiment due to smaller changes in hydrostatic pressure inside the confining tube.

A comparison of pressure history near the implodable volume (ch-B sensor) for all Geo B is shown in Fig. 14(a). As the Geo B0 shows a monotonic decay in pressure during implosion process, the presence of air bubble in Geo B15 and Geo B30 leads to higher pressure hammer wave with increasing hammer with air bubble size. The hammer wave shows strength of $0.7 P_c$ for Geo B30 and $0.4 P_c$ for Geo B15. This increasing trend in hammer wave indicates that a further increase in air bubble size might lead to even more enhanced implosion waves, which can surpass the hydrostatic pressure value resembling typical free-field implosion shock waves [1, 2, 15]. The comparison of mean pressure inside the confining tube shows similar results as seen for Geo A. The residual pressure increases with increasing air bubble size and

there are $\sim 0.15 P_c$, $0.25 P_c$ and $0.35 P_c$ residual pressures for Geo B0, Geo B15 and Geo B30 respectively.

Figure 14 (a) Pressure history near implodable volume and (b) the mean hydrostatic pressure history comparison between Geo B0, Geo B15, and Geo B30

3.2.3 Total change in volume of the implodable and post-mortem images

Using the approaches outlined in [16], the total volume change in the implodable can be correlated with the change in mean pressure in the confining tube in following way (assuming air bubble expansion process as isothermal process at the end of implosion experiment):

$$V_{water} + V_{implodable} + V_{air-bubble} = V_{confining} \quad (1)$$

$$V_{implodable} = \left[\pi R^2 L \left(1 + \frac{P_{residual} R}{2hE} (5 - 4\nu) \right) \right] - \left[V_{water_i} \left(1 + \frac{(P_c - P_{residual})}{K} \right) \right] - \left(\frac{P_c}{P_{residual}} \right) V_{air-bubble-i} \quad (2)$$

$$\begin{aligned} \Delta V_{implodable} &= V_{implodable_i} - V_{implodable} \\ &= (P_c - P_{residual}) \left[\pi R^2 L \left(\frac{1}{K} + \frac{R}{2hE} (5 - 4\nu) \right) \right] + \left[\left(\frac{P_c}{P_{residual}} \right) - 1 \right] V_{air-bubble-i} \end{aligned} \quad (3)$$

Using the residual pressure ($P_{residual}$) during the experiments, total value of $\Delta V_{implodable}$ can be calculated from Eq.(3) and the results are shown in Table 3. For comparison, experimentally observed $\Delta V_{implodable}$ is also shown in Table 3. It is seen that Eq. (3) can predict the total $\Delta V_{implodable}$ reasonably well. In general, a significant change in the $\Delta V_{implodable}$ is seen due to the addition of air bubble in experiments. $\Delta V_{implodable}$ increases by almost 37% times in addition of 30% air bubble in Geo A in experiments. For Geo B experiments, $\Delta V_{implodable}$ increases by upto 2.5 times and 3.5 times with the addition of 15% and 30% air bubble respectively.

Geometry	$P_{residual}$ from Experiments	$\Delta V_{implodable}$ from experiments	$\Delta V_{implodable}$ from Eq. (3)	$\Delta V_{implodable} / V_{implodable_initial}$
Geo A0	0.46 MPa	98 ml	75 ml	46% (experimental) 35% (analytical)
Geo A30	1.28 MPa	135 ml	146 ml	63% (experimental) 68% (analytical)
Geo B0	0.51 MPa	90 ml	74 ml	16% (experimental) 13% (analytical)
Geo B15	0.74 MPa	221 ml	350 ml	40% (experimental) 63% (analytical)
Geo B30	1.13 MPa	304 ml	366 ml	54% (experimental) 65% (analytical)

Table 3 Comparison of residual pressure ($P_{residual}$) and the final change in volume of implodable ($\Delta V_{implodable}$)

The post-mortem images for all the experiments conducted are shown in Fig. 15. The progression of collapse is found to increase further along the length in Geo A30 as shown in Fig. 15(b) as compared to Geo A0. The side view showing the lobes and valleys also indicates marginally higher collapse.

The post mortem images for Geo B0, Geo B15 and Geo B30 clearly shows increasing collapse with the addition of air bubble in both front view and side view. No wall contact is present for Geo B0, while both Geo B15 and Geo B30 exhibit wall contact in side view images. The lobe formed for Geo B30 also shows a diametrical line contact along with large longitudinal contact region while Geo B15 neither exhibit diametrical wall contact nor the longitudinal contact region.

Figure 15 Post-mortem Images (a) Geo A0 (b) Geo A30 (c) Geo B0 (d) Geo B15 (e) Geo B30

6. Conclusions

An experimental study was conducted to understand the effect of trapped air bubble on the mechanics of implosion inside a confining environment using 3-D DIC technique. The key findings of this study are as follows:

(1) The calibration using a submerged calibration grid can successfully account for the refractive index mismatch between the water/acrylic window/air. The calibration experiments reveal that the both the in-plane and out-of-plane measurements can be measured using this modified 3-D DIC calibration procedure within 4% error.

(2) The mechanics of deformation during a confined implosion experiment is different than a free-field implosion experiment. The deformation process in a confined implosion consists of four key stages: (a) initially accelerating recession of wall reaching the peak velocity, (b) the deceleration of the walls prior to wall contact initiation, (c) wall contact initiation and progression of wall contact along the central diameter in a deceleration manner, and (d) decelerated propagation of buckle towards the end-caps of the implodable volume. If size of geometry is large as compared to the confining tube, wall contact/buckle propagation might not occur.

(3) The addition of air bubble increased the mean hydrostatic pressure inside the confining tube leading to stronger hammer pressure wave with increasing air bubble size. The hammer pressure magnitude increased upto $0.7 P_c$ in the case of Geo B and upto $0.8 P_c$ in the case of Geo A.

(4) Although the relative smaller size of Geo A leads to negligible changes in velocity at the center point and small changes in velocity during buckle propagation with the addition of air bubble, the overall change in volume increases by 2 times indicating further propagation of buckle.

(5) Geo B shows a dramatic change in deformation mechanics during implosion with the addition of air bubble. With no air bubble, a partial collapse is observed with no

wall contact with a peak velocity of 14 m/s. With the addition of 15% and 30% air bubble, the peak velocity increases to 16.5 m/s and 18 m/s respectively. Although, the deceleration followed by the peak velocity is still present in all the cases, the extent of collapse increases significantly.

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CHAPTER 8

SHOCK INITIATED IMPLOSION OF METALLIC TUBES IN A CONFINED ENVIRONMENT

by

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Abstract

The shock initiated implosion of cylindrical metallic shells was experimentally investigated in confining environments. The experiments were conducted at varying hydrostatic pre-pressures ranging from 70% of the critical pressure (P_c) to 95% P_c inside the confining tube. Two different geometries of implodable volumes, 31.8 mm outer diameter and 50.8 mm outer diameter were utilized to study the effect of relative size of implodable volume. For initiation of shock wave inside the confining tube, an RP-80 detonator was placed at a stand-off distance of ~ 530 mm from the implodable volumes. The high speed photography coupled with 3 D digital image correlation technique was utilized to capture the real-time full field deformation of the implodable volume during the experiments. To incorporate 3-D digital image correlation technique for measuring real-time deformations, initial calibration experiments were performed to quantify the measurement error introduced by the refractive indices of the water and window. It is concluded that the calibration of the intrinsic/extrinsic parameters of the cameras using a submerged calibration target provides accurate in-plane and out-of-plane displacements. The study shows that there are four possible situations that can occur in shock initiated implosion problems depending upon level of pre-pressure: 1) lowest pre-pressure $\rightarrow$ elastic vibrations with no plastic deformation, 2) elasto-plastic vibrations with residual plastic deformation, 3) elasto-plastic vibrations accompanied by a delayed collapse of the implodable volume from the secondary pressure wave and 4) direct collapse of implodable volume from the shock wave. Experiments also indicated the vibration frequency is significantly lower than the free flexural vibration frequencies and found to reduce by a factor upto 3.3

times in these experiments. These lower frequencies are primarily caused by two effects: 1) the effect of initial pre-stress in the implodable volume due to the presence of hydrostatic pre-pressure and, 2) the added mass of water surrounding the implodable volume. Further analytical consideration of these two factors result allows great agreement between experimental and analytically calculated frequencies.

Keywords

Implosion, Implodable Volume, Collapse, Pressure Waves, Computational Modeling, Fluid Structure Interaction, Confined Environment, Shock Wave, Underwater Explosion (UNDEX), Vibration Frequency, Pre-stress, High Speed Photography, 3-D Digital Image Correlation

1. Introduction

The effect of underwater explosive loading (UNDEX) on the implosion mechanics of metallic tubes inside a tubular confining space is experimentally investigated. Two different outer diameter (31.8 mm and 50.8 mm) cylindrical Al 6061-T6 tubes were studied which were placed concentrically inside the confining tube during experiments. The experiments are conducted at varying pre-hydrostatic pressure inside the confining tube, and an UNDEX placed inside the confining tube is used to impart the shock wave loading on the implodable volume. Several pressure transducers mounted on the wall of the confining tube and two high-speed cameras are used to capture the pressure history and the real-time images of the implosion event

respectively. The high-speed photography coupled with 3-D digital image correlation (DIC) is incorporated to understand the onset of collapse of the implodable volume.

Cylindrical metallic shell structures are extensively used in the design of many underwater structures, such as deep ocean submersibles, submarines, autonomous underwater vehicles (AUVs), underwater pipelines etc. [1–3]. Often, these structures contain gas inside, at a pressure lower than the environment's, to ensure proper functionality of electronics and other major components. The external pressure of water at extreme sea depths may cause the enclosed shell to become structurally unstable and collapse onto itself (implode). The resulting collapse is violent and causes a rapid release of energy in the form of shock waves, high velocity fluid motion, and sound [4]. In recent years, many catastrophic implosion events such as the super Kamiokande laboratory accident, where over 7000 photomultiplier tubes collapsed [5], and the implosion of the Nereus AUV [6], have shown the importance of better understanding the implosion phenomenon in order to create safer underwater structures. For this reason, this topic is of great interest to naval and marine communities.

There have been several investigations reported by researchers in naval and marine communities on the mechanics and fluid-structure interaction during a “free-field natural implosion” process [7–12,3,13–17]. Turner measured the near-field pressure history during the implosion of glass spheres in a free-field environment and concluded that the failure time history of the structure has a significant influence on an implosion pressure pulse [12]. Later, both Turner and Ambrico, and Farhat et al. studied the implosion of aluminum cylindrical tubes experimentally and numerically

[3,13]. Turner and Ambrico identified the key features of a typical mode-2 implosion process by correlating the important signatures of the pressure pulse with the structural deformation[3]. Farhat et al. extended this work by comparing both mode-4 and mode-2 implosion, and showed that the higher mode of collapse generates a higher pressure peak but of smaller duration when compared to a lower mode collapse [13]. Recently, Gupta et al. captured the real-time collapse mechanics of an implodable volume by using a high speed DIC technique [14]. The study showed that the time period and impulse of the positive pressure wave directly correlates to the time taken for the collapse to propagate along the length of the implodable volume.

Due to recent interest in the implosion occurring in a confined environment, the authors have previously investigated the natural implosion of different geometries in tubular confining spaces along with understanding the effect of relative placement of implodable volume inside the confining space [18,19]. There still exist limited experimental findings which address the problem of structural implosion occurring due to a shock wave/UNDEX loading. Although a similar problem has been theoretically investigated since early 1960's in terms of "dynamic buckling" of cylindrical shell structures [20–22], there are very few comprehensive experimental studies reporting "underwater" dynamic buckling of structures. A recent study on underwater buckling of tubular structures in a confining space was reported by Bitter and Shepherd [23]. They studied the dynamic buckling of submerged tubular structures in a rigid closed outer tube. Dynamic buckling experiments were performed by applying impulsive loading in the water filled outer tube using a projectile and the fluid structure interaction models were developed to predict the dynamic response of

the tube. In their experiments, the amplitude of impulse loading was an order of magnitude higher than the critical static buckling threshold, therefore the impulsive loading was only considered to cause buckling in the specimen and the effect of initial hydrostatic pressure was not specifically investigated. To the best of author's knowledge, there have been no studies reported on investigating the effect of hydrostatic pressure on shock initiated buckling/implosion of structures in confining environments. In real submarine situations, the hydrostatic pressure always acts on the structure (or the structures are always pre-hydrostatically loaded with certain value); hence an additional shockwave loading, which might not cause any damage to a structure at no hydrostatic pressure, may cause it to fail due to the presence of initial hydrostatic pressure. Therefore, the dynamic buckling design criteria must take into account for the typical operating hydrostatic pressure for safes design of the underwater structures. Due to nonlinear nature of deformation of these structures under hydrostatic pressure, the stiffness of structure tends to degrade significantly when reaching near the critical buckling threshold value and it results into structure being more susceptible to failure near critical pressure when subjected to shock loads.

The following study aims to address this gap by conducting UNDEX initiated implosion experiments for two different geometries. Additionally, the full-field 3D DIC measurement technique is used to understand the evolution of structural vibrations and damage. The theoretical models proposed by bitter and shepherd are applied to estimate the elastic vibration response of the structures.

2. Experimental Setup

2.1 Confining Tube Facility and Implodable Volume

A schematic of the experimental setup for the tubular confined space implosion facility is shown in Fig. 1. The confining tube has an inner diameter of 177.8 mm (7 in) with 19 mm (0.75 in) wall thickness and is made out of seamless low-carbon steel (SA106-B) pipe. The overall internal length ($2L$) of the confining tube is 2.29 m (90 in). The confining tube is fabricated in a modular design. It contains two sections of 914 mm (36 in) length separated by a 457 mm (18 inch) long window section for high-speed photography of the implosion process (see Fig. 1). This central section has a 63.5 mm (2.5 in) thick flat acrylic viewing window with a viewable area of approximately 216 mm x 102 mm (8.5 in x 4.0 in). A cross-sectional view of the central section is shown in the inset of Fig. 1. It must be noted that insertion of the window results into $\sim 15\%$ difference between the front/back cross-sectional areas.

The implodable volumes used in this study are comprised of commercially available aluminum 6061-T6 extruded seamless tubing. A schematic of the mounting fixture for holding the implodable is shown in Fig. 2. Table 1 provides a summary of the experiments conducted and the details of the specimen used in this study. Two geometries of the implodable volumes are studied: (1) 31.8 mm outer diameter (referred as Geo A later in the article), and (2) 50.8 mm outer diameter (Geo B). The implodable volumes are sealed on both ends using solid aluminum end-caps, which utilize circumferential o-rings for sealing the specimen for underwater experiments. This tube and end-cap assembly is mounted inside the confining tube using support

collets, which ensure concentric placement of the implodable in the confining tube (see Fig. 2). The construction of the three spoke collet is chosen so as to provide minimal interference to the axial pressure waves and water flow within the confining tube during the implosion event. The confining tube is filled and pressurized with water using a hydrostatic test pump. The confining tube is inclined at $\sim 2^\circ$ at one end manually in order to remove the trapped air bubble in the confining tube to the maximum extent possible for no air bubble experiments. High frequency dynamic pressure transducers (PCB-113B22) are installed in the wall of the vessel at various axial locations such that the transducer sensor element is flush with the inner surface (see Fig. 3 for exact locations).

Figure 1 Schematic of confined tube implosion facility

A pair of Photron SA1 high speed cameras, as shown in Fig. 1, is placed in front of the viewing window at a distance of ~ 1.3 m with a stereo angle of 13° . The

high speed images are captured at a frame rate of 40,000 fps for Geo A and at 30,000 fps for Geo B.

Figure 2 Schematic of the implodable volume with end-caps and support collets

Figure 3 Schematic of the implodable volume with end-caps and support collets

Expt. Label	Outer Diameter	Critical Collapse Pressure (P_c)	Initiation Pressure	Peak Shock Magnitude at Ch-B	Implosion (Y/N)	Bubble Pulse Arrival Time
Geo A	31.8 mm (1.25 in)	3.55 MPa (515 psi)	70% P_c	9.55 MPa	N	1.19 ms
			76% P_c	10.34 MPa	N	1.10 ms
			80% P_c	8.97 MPa	Y	1.05 ms
			85% P_c	9.76 MPa	Y	1.00 ms
			90% P_c	9.00 MPa	Y	0.96 ms
Geo B	50.8 mm (2.00 in)	3.38 MPa (490 psi)	79% P_c	10.03 MPa	N	1.11 ms
			84% P_c	9.62 MPa	N	1.06 ms
			90% P_c	9.72 MPa	Y	1.00 ms
			94% P_c	10.14 MPa	Y	0.96 ms

Table 1 Layout of the experiments conducted in this study

2.2 Shock Wave Loading using UNDEX

To generate a shock wave inside the confining tube, a RP-80 detonator (from Teledyne RISI, see [24] for details) is utilized in the experiments. The RP-80 detonator consists of 80 mg Pentaerythritol tetranitrate (PETN) as initiating explosive and 123 mg RDX as output explosive. The detonator is placed at ch-3 longitudinal location along the central axis of the confining tube (as shown schematically in Fig. 3). Thus, the stand-off distance between the charge and the center of the implodable volume is 686 mm (27 in). Such stand-off distance (> 3 times the inner diameter of confining tube) is chosen in order to fully develop the shock wave in radial direction.

A typical shock wave recorded at the closed sensor ch-3 (which is at a stand-off distance of 89 mm (3.5 in)) is shown in Fig. 4. The shock wave has a rise time of 3 μ s with a peak reflected pressure of 55 MPa (8000 psi) at ch-3. After travelling approximately a diameter of the confining tube, the shock wave peak-pressure almost vanishes and the multiple reflections generated from the inner walls of the confining tube form a propagating shock wave towards the implodable volume as shown in Fig. 4. It can be seen that the shock wave observed at ch-5 travels in an almost steady manner with a slow decay in magnitude with distance. The shock wave seen at the center of implodable volume is almost triangular in shape and has a peak magnitude of 12 MPa with a loading duration of ~ 230 μ s. Please note that there exists a relatively higher decay in peak pressure from ch-4 (152 mm prior to window section) to ch-B (center of the window section) due to change in cross-sectional of the confining tube. When the shock wave passes beyond the implodable volume and reaches the other

end-plate (ch-8), the compressive wave reflects which can be seen as another following pulse seen at ch-7 as shown in Fig. 4.

Figure 4 Shock wave generated using RP-80 detonator. The data for closed sensor (ch-3) has been clipped in y-axis. The peak dynamic magnitude at ch-3 is 55 MPa. Note that the shock wave pressure pulse seen at each sensor location is shown only and the pressure oscillations beyond the shock wave pulse have been trimmed to avoid ambiguity.

2.3 Hydrostatic Pre-pressure at Initiation

The initial pre-pressure present inside the confining tube is a critical parameter in shock initiated dynamic buckling problem. The impulse imparted from the shock wave is constant throughout the study hence; the initial hydrostatic pre-pressure determines the critical degree of excitation needed in order to cause instability in the implodable volume. At pre-pressures close to the critical pressures, the structural stiffness of implodable volume reduces significantly and the shock wave loading may have sufficient strength to excite the implodable volume beyond the stability point. On the other hand, if the initial pre-pressure is significantly lower, the shock wave might only implodable volume to undergo vibrations not resulting into an implosion/a collapse. Hence, the initial pre-pressure becomes a deciding factor whether an implosion will occur or not. In a general scenario, three cases can be observed in

shock initiated problems: (1) elastic vibrations: initial pre-pressure is low enough such that the shock wave can only excite the implodable volume to vibrate within the elastic limit. After exposure to such shock wave loading, there is no damage accumulated in the implodable volume. (2) elasto-plastic vibrations: initial pre-pressure is large enough such that shock wave can excite the implodable volume to vibrate *beyond* the elastic limit. Thus, a permanent plastic deformation is present after the exposure to shock wave loading leading to damage accumulation in the implodable volume. Although no implosion occurs in such a situation, further subjection of implodable volume to same strength shock waves may cause it to implode due to decrease in critical pressure after plastic deformation. (3) implosion: initial pre-pressure is so large such that shock wave loading causes implosion. It must be noted that the implosion may/may not be preceded by the vibrations in the implodable volume. After the shock wave excites all the flexural modes in the implodable volume, the shock energy is distributed in each mode. Thus, implodable volume may not have sufficient excitation initially to surpass the implosion threshold of the major dominating mode (which is mode-2 (2 waves in circumferential direction, 1 wave in length direction) in this study). But, the transfer of energy in between modes may contribute to the implodable volume to surpass the threshold [20]. The increase in pre-pressure beyond such limit may cause the implodable volume to undergo implosion following the shock wave loading.

In this study, the shock initiation is conducted at several hydrostatic pre-pressures, ranging from 70% to 90% of the critical buckling pressures of the

implodable volumes as shown in Table 1. These pre-pressures are chosen in order to observe both vibrations and the collapse during the shock initiated experiments.

2.4 3-D Digital Image Correlation Calibration Setup

Application of 3-D digital image correlation technique for submerged applications requires rigorous two-step calibration procedure, as outlined in [25]. Although this procedure has been shown to accurately measure both the three dimensional position and the displacements/strains of the submerged objects, the procedure of calibration and correction is complex, and it requires extensive post-processing of the data. Recently, It has also been shown that calibration of extrinsic and intrinsic parameters by using a submerged calibration target can result into high accuracy in both in-plane and out-of-plane displacement measurements using 3-D DIC [14]. As the primary motive of using 3-D DIC in this study is to measure out-of-plane displacements/velocity in the confined implosion experiments, the authors have used the similar submerged calibration procedure as shown in [14] .

To evaluate the accuracy of measured DIC displacements in a submerged situation, calibration experiments are conducted in a custom designed tank (shown in Fig. 5), which replicates the design of the middle section of the confining tube. This setup replicates the optical effects of submersion in the middle section of the confining tube using a more accessible smaller tank. The setup is approximately 400 mm (16 in) wide and 400 mm (16 in) long with a height of ~ 200 mm (8 in). The front viewing window utilized in the custom designed tank setup is identical to that of the middle section's window in confining tube. A 250 mm x 100 mm (10 in x 4 in) flat aluminum specimen is placed inside the tank on a precision translation stage, which can provide

translations with 0.01 mm accuracy. The aluminum specimen is placed at ~ 70 mm (2.75 in) deep in water measured from the back of the viewing window, which represents the typical location of the implodable volume in the middle section of the confining tube. A random high contrast black/white speckle pattern is painted on the front surface of the specimen for DIC measurements.

Figure 5 Custom designed tank setup for underwater DIC calibration experiments

The SA-1 high-speed cameras, with the same front-end optics, are used to capture images of the translated specimen during calibration experiments. The resolution of the camera image is 1024×1024 pixels, corresponding to an approximate magnification factor of 4.02 pixels/mm. Calibration of intrinsic and extrinsic parameters is performed using a submerged calibration grid (12 dots x 9 dots, 7 mm interspacing) provided by Correlated Solutions, Inc.[26]. Using the translation stage, the specimen is translated in 1 mm increments in both in-plane and out-of-plane

directions and the corresponding images are captured. Displacement of the specimen at each translation increment is estimated with Vic-3D 2012 software using 25×25 pixels subsets and a step size of 7 [26].

3. Experimental Results and Discussion

3.1 Underwater 3-D DIC Calibration Results

The out-of-plane 3D DIC calibration results for submerged objects are shown in Fig. 6. Both displacements and strains (von-Mises) are calculated from the 3D DIC measurements. To quantify the accuracy of the measured displacement and strains, the average surface displacement and standard deviation over the whole area for each given displacement is calculated. Note that the as rigid translation is not supposed to produce any strains on the specimen; the measured von-Mises strain from DIC (pseudo strain) is an artifact due to the change in refractive index due to water and the viewing window in these experiments. The range of pseudo strain is calculated across the area such that 95% of the values lie in the range (which equals to two standard deviation interval from theory of Gaussian distribution). A plot of the actual out-of-plane displacement and the measured average DIC displacement of the specimen is shown in Fig. 6(a). The measured average DIC displacements are in good agreement with the true displacements and the percentage error between true and measured out-of-plane displacement is less than 4%. The maximum range of pseudo strains is found upto 0.08% (see Fig. 6(b)), which is significantly higher than the strain accuracy of the 3D DIC technique ($\sim 0.02\%$ from [27]) in air.

Figure 6 Underwater calibration results (a) out-of-plane displacement results (b) out-of-plane pseudo strains (c) in-plane displacement results (d) in-plane pseudo strains

The in-plane 3D DIC calibration results for submerged objects are shown in Fig. 6(c). A comparison of actual in-plane displacement and measured average DIC displacement as shown in Fig. 6(c) indicates that the two are in very good agreement. The magnitude of error is $< 1\%$ for all in-plane displacements. The range of pseudo strains is found to be upto 0.11% (see Fig. 6(d)). As the measurement of out-of-plane displacements is the primary interest in the implosion experiments, it can be concluded that both in-plane and out-of-plane displacements can be measured with adequate accuracy using this modified DIC calibration in the case of submerged object viewed through a single flat window. The extraction of camera's intrinsic and extrinsic parameters using submerged calibration grid can successfully account for the change in refractive index due to water and acrylic viewing window.

After the successful establishment of the modified 3D DIC calibration procedure for submerged objects viewed through a single flat window, high speed cameras are moved to the confining tube implosion facility. Cameras are placed at approximately same distance from the viewing window as in the calibration experiments. To disregard any differences present between the calibration setup and the window section of the confining tube, cameras are recalibrated using the same submerged calibration grid placed inside the middle section. The calibration grid is moved in the region encompassed by the largest implodable volume used in this study (Geo B). Once the calibration process is completed, the accuracy of 3D DIC displacement measurements in the confining tube implosion facility is re-estimated by quantifying the radius of both Geo A and Geo B implodable volumes using 3D-DIC technique. After reconstructing the cylindrical shape of the implodable volume using Vic-3D 2012 software, the local radius of curvature of the specimen surface is estimated (see Fig. 7) using data from the central region of the specimen. The results for both the geometries are summarized in Table 2. It can be seen that the measured average radius of the specimens using 3D DIC matches very well with the true radius for both of the geometries with a maximum error of 2%. Thus, it can be established that both the shape and 3-D deformation of submerged objects can be measured accurately using the modified calibration process described in Sec. 2.4.

Geometry	True Radius	Measured Average DIC Radius (95% Confidence)	Percentage Error
GeoA	15.88 mm	15.68 mm $\pm$ 0.20 mm	1.26%
GeoB	25.40 mm	25.09 mm $\pm$ 0.28 mm	1.22%

Table 2 Comparison of measured average radius from DIC with the true radius of the implodable volumes

Figure 7 Measurement of the radius of the implodable volume using 3-D DIC. (a) Geo A (31.8 mm OD) (b) Geo B (50.8 mm OD)

3.2 Quasi-static Non-linear Deformation/Energy Curve

Earlier researches on the post-buckling behavior of cylindrical shells have shown that the quasi-static non-linear pressure deformation curve of an implodable volume characteristically defines the change in structural stiffness in a buckling problem [28,29]. The cylindrical shell contains maximum stiffness at zero pressure, but with increasing deformation and pressure in pre-buckling regime, the structural stiffness degrades and vanishes completely at the point of instability. Beyond this point, the pressure needed to continue deformation decreases with increasing deformation indicating the presence of negative stiffness. This negative structural stiffness also decreases with increasing deformation making structure more compliant.

In this study, this pressure deformation curve is numerically calculated using ABAQUS static Riks analysis and is shown in Fig. 8(a) and (c) (see more details in Appendix A). In order to make comparison between two geometries used in this study, the curves are normalized with respect of critical buckling pressure in y-axis and with the radius on x-axis. The shape of normalized curve is approximately similar for both the geometries. Both achieve a pre-buckling deformation value of $\Delta/R = 3.8\%$ at the

critical buckling pressure, which shows that a significant deformation is present in the implodable volume at the critical pressure. This phenomenon is generally attributed to the imperfection present in the cylindrical shells [30].

Figure 8 Nonlinear quasi-static analysis of implodable volumes (a) pressure deformation curve in the pre-buckling region (b) change in stiffness and frequency as a function of pre-pressure (c) complete pressure deformation curve (d) minimum deformation required and energy barrier for implodable volume as a function of pre-pressure

3.2.1 Estimation of Structural Stiffness Degradation with Increasing Pressure

In qualitative terms, the structural stiffness (K) of a cylindrical shell can be defined as the slope of pressure deformation curve (see Fig. 8(a)) which can be written

as $K|_{P=P_0} = \frac{dP}{d\Delta}|_{P=P_0}$. This stiffness is calculated for both geometries and is plotted as a

function of normalized pressure in Fig. 8(b). The stiffness is normalized with the

stiffness at zero-pressure ($K|_{p=0}$) as it indicates the highest stiffness of the structure.

This graph shows that the structural stiffness degrades almost linearly with increasing pressure. Using the standard theory of vibrations, the oscillation frequency is proportional to the square root of the stiffness of a structure ($\omega \propto \sqrt{K}$). Hence, in order to visualize the effect of hydrostatic pressure on the oscillations frequency of the structure, normalized vibration frequency ($\omega/(\omega|_{p=0})$) is also plotted in Fig. 8(b). It shows that the oscillation frequency decreases with increasing hydrostatic pressure. As the shape of the curve is similar to an inverted parabola, the effect of hydrostatic pressure is not dominant for lower pressures. As the hydrostatic pressure increases further, a higher relative change in oscillation frequency is seen. This analysis suggests that the initial pre-pressure present in the confining tube alters the vibration frequency due to pre-loading on the implodable volume (if effect of surrounding water is neglected, later discussed in detail in Sec. 3.5). Moreover, when the implodable volume is subjected to shock wave loading, the vibration amplitude will increase with increasing pre-pressure because of relative degradation in structural stiffness (a less stiff structure should generate higher amplitude of oscillations with respect to a relatively stiffer structure).

3.2.2 Minimum Deformation/Energy Requirement for Implosion with Varying Pre-pressure

The experiments conducted in this study are initiated at a certain pre-pressure below the critical pressure, thus a typical state of the implodable volume can be represented by the point *A* in Fig. 8(c). When a shock is initiated in the near field, a

finite dynamic deformation is achieved during the shock wave loading in the implodable volume. Unless this deformation exceeds the state of implodable volume beyond point B , the implodable volume will stay in a stable equilibrium. Therefore, the impulse/strength of the shock wave loading must be sufficient enough to deform the implodable volume beyond point B . Moreover, this required deformation $(\Delta_B - \Delta_A)$ increases with increasing gap between the critical pressure and initiation pressure $(\Delta P = P_c - P_{in})$, which is plotted in Fig. 8(d). For the experiments conducted in this study, the initial pre-pressure is kept between 90% and 70% P_c and hence, $(\Delta_B - \Delta_A)/R$ required from Fig. 8(c) should be between 7% and 14% respectively. This theoretical deformation can be perceived as a lower bound of excitation needed for to trigger the instability using shock wave loading.

In the terms of energy, the progression in deformation results into increasing strain energy absorbed in the implodable volume. Thus, the energy difference between the point A and B (E_A and E_B) represents the energy barrier/gap to surpass the stability point. Similar to the $(\Delta_B - \Delta_A)/R$ curve, this energy gap increases with ΔP as shown in Fig. 8(d). As the incident shock wave from explosive essentially imparts acoustic energy onto the implodable volume, the acoustic energy smaller less than this energy gap will never theoretically collapse the implodable volume and the structure can be considered safe in these situations. Hence, this energy gap represents the upper bound of the incident shock wave's energy that an implodable volume can sustain without imploding at a given pre-pressure.

The comparison between Geo A and Geo B as shown in Fig. 8(d) shows that the energy requirement for Geo B to go unstable is approximately three times higher

than Geo A because of the larger volume of material in Geo B. Thus, a bigger sized geometry is safer in design in comparison to a smaller geometry.

3.3 Pressure History Analysis

The pressure history inside the confining tube shows some characteristic differences depending upon occurrence of implosion in the experiments. The pressure history for the minimum and maximum pre-pressure experiments conducted for both of the geometries is plotted in Fig. 9. Due to complex nature of UNDEX inside the confining tube (expansion/collapse of resulting air bubble [31]), the data captured for pressure history tends to be noisy after the passage of primary shock wave shown in Fig. 9. The incident pressure wave observed for all the experiments are very similar as shown in Fig. 9 indicating that the structural response of the implodable volume, while propagating over the length of the implodable volume, is minimal and the nature of initial shock wave does not change with the pre-pressure.

Interestingly, the shock wave seen at ch-4 (at a stand-off distance of 305 mm) always contains a relatively lower pressure wave following in later time (between 0.75 ms to 1.0 ms) and the higher pre-pressure shows an early arrival of this lower pressure wave. This observation suggests that this wave is primarily due to the collapse of the air bubble resulting from UNDEX. For further analysis, the time of arrival for first bubble pulse is estimated using the parameters shown by Shin [32] and is shown in Table 1. The arrival time changes from 0.96 ms to 1.17 ms with increasing pre-pressure in the experiments, which agrees well with the observation made in Fig. 9. Authors also would like to note that the reflection of the first shock wave from the near-end plate (ch-8 sensor) will also result in a compressive wave after $\sim 650 \mu\text{s}$,

which is seen as a tiny change in pressure prior to major bubble pulse arrival (this wave tends to merge with the bubble pulse in the higher pre-pressure experiments due to earlier arrival of bubble pulse). Hence, secondary pressure pulses (bubble pulses and reflected shock wave) are also present in the shock wave loading experiments presented in this article.

Figure 9 Pressure history inside the confining tube (a) Geo A at $70\% P_c$, (b) Geo A at $90\% P_c$, (c) Geo B at $80\% P_c$, (b) Geo A at $94\% P_c$. The data for different channels has been shifted in y-axis for clarity. The dashed line represents the hydrostatic pre-pressure of the experiment. The dash-dot line represents atmospheric pressure.

The low pre-pressure experiments shown in Fig. 9(a) and (c) do not exhibit implosion, while the highest pre-pressure experiments shown in Fig. 9(b) and (d) exhibit implosion. The close inspection on the pressure history shows that the occurrence of implosion can be identified in the pressure history in sensors away from the explosive. The pressure magnitude at ch-4 in high pre-pressure experiments

decreases with respect to corresponding lower pre-pressure experiments. Moreover, both ch-5 and ch-7 start showing cavitation between $t = 2.0 - 2.5$ ms, and ch-7 (which is close to the end-plate) shows larger cavitation durations than ch-3 in Fig. 9(b) and (b). This phenomenon of cavitation has been previously observed by the authors in the natural implosion experiments in the confining tube, which indicates the arrival of implosion wave at the end-plates [18,19].⁶ As the magnitude of these wave seen in [18,19] is smaller than the collapse pressure, these waves overlap with the noisy post shock wave pressure signals and it is hard to explicitly distinguish the implosion wave.

3.4 Deformation History from 3D DIC

3.4.1 Initial Impact of the Shock Wave/Longitudinal Flexural Waves in the Implodable Volume

The UNDEX inside the confining tube results into a longitudinally travelling shock wave towards the specimen as shown in Fig. 10(a). The travelling speed of the wave is approximately equal to the coupled pressure wave speed inside the confining tube, which is shown to be 1395 m/s in the author's previous study [18]. Hence, it takes ~ 220 μ s to travel across the unsupported length of the implodable volume (see schematic in Fig. 10(a)). The shock wave generated is radially symmetric in nature due to the cylindrical geometry of the confining tube. Therefore, the subjection of implodable volume to ~ 9.5 MPa peak amplitude shock wave results into an axisymmetric local compression as shown in Fig. 10. This local compression excites

⁶ It must be noted that the data for the end-plate near the UNDEX, ch-1, is not discussed as the oscillation of bubble pulse suppresses these features.

longitudinally travelling flexural modes in the implodable volume. In addition, the travelling nature of shock wave itself imparts the entire unsupported length of the implodable volume with local compression for duration of 220 μs . The interaction of these two phenomenon leads to longitudinally travelling axisymmetric waves as seen in all the experiments as shown in Fig. 10(b). This figure clearly shows the alternate regions of increased and decreased radius evolving from the left side of the implodable volume (the end where the shock wave impacts). With increasing time, these regions move towards the shock wave directions indicating travelling axisymmetric waves. The amplitude of the vibration is increased with increasing time due to higher impulse imparted on the implodable volume and it increases from 0.4% $\Delta R/R$ to $\sim 1.2\%$ $\Delta R/R$ between 50 μs and 175 μs after impact. These waves are more prominent in Geo A due to their higher slenderness ratio with respect to Geo B, hence Geo A is only shown in Fig. 10(b). These longitudinal waves are observed for $\sim 200 - 300 \mu\text{s}$ after the impact of the shock wave in Geo A.

Figure 10 Evolution of longitudinally travelling axisymmetric waves in the implodable volume. Time $t = t_1$ represents the instance of impact of the shock wave at left end of the implodable volume)

This observation also indicates that although the primary mode of collapse for these tubes is mode-2, the dynamic nature of shock wave's longitudinal impact primarily excites the higher order axisymmetric longitudinal modes only.

3.4.2 Transition to Mode-2 Vibrations

When a structure is excited with a broad-range frequency force (a shock wave in this study), various frequencies of the structure are excited. But later with time, the cyclic transfer of energy between mode-shapes [20] may allow the lowest vibration frequency mode to dominate. For the experiments conducted in this study, similar behavior is observed during the experiments. A typical mode-2 evolution for GeoA and GeoB is shown in Fig. 11. Both geometries exhibit the longitudinal higher order vibrations followed by the transition phase. During this phase, these longitudinal higher modes slowly disappear and the longitudinal modes consolidate into two half sine waves in longitudinal direction leading to a local higher and a lower radius region. These regions (as shown in Fig. 11(c)) move in two opposite circumferential directions and start forming the mode-2 shape. By time $t = 1.09$ ms, Geo A reaches a definite mode-2 shape with a maximum valley deformation of $4\% \Delta R/R$. After reaching the peak deformation, the direction of deformation reverses and by time $t = 1.46$ ms, the implodable volume reaches the mean position of the mode-2 vibration. Later by time $t = 1.84$ ms, the same point on the implodable volume becomes a lobe with $\sim 2.5\% \Delta R/R$ deformation. DIC deformation data also shows that the valley and lobes are moving longitudinally during the mode-2 vibrations observed for lower pre-pressure experiments. This shows the presence of residual longitudinal vibration modes excited by the shock wave which couples with mode-2 vibrations results in

“longitudinally travelling mode-2 vibrations”. Similar qualitative deformation behavior is seen for Geo B as shown in Fig. 11(b), except the time period of mode-2 vibration is larger indicating slower structural vibrations.

Figure 11 $\Delta R/R$ contours for (a) Geo A and (b) Geo B. The contours shown are for 70% pre-pressure experiment for Geo A and for 79% pre-pressure experiment for Geo B.

3.4.3 Effect of Pre-pressure on the Implosion Mechanics

The structural stiffness degradation as shown in Sec. 3.2.1 with increasing pre-pressure induces structure to become unstable and collapse upon the shock wave loading. For investigating such instability initiation with varying pre-pressure, the peak deformation observed for all Geo A and Geo B experiments is shown in Fig. 12 and 13. The time $t = 0$ ms indicates the initiation of explosive loading and it takes ~

0.4 ms for the shock wave to reach the implodable volume. Fig. 12 plots the radial deformation for Geo A at three following circumferential locations: 1) valley represented as P0, 2) Intermediate location at 45° offset (P45), and 3) lobe represented as P90. In the experiments, it is observed that the formation of lobe and valley in the implodable volumes varies depending upon the specimen and thus the high speed DIC data may/may not capture both the lobes and valley in a single experiment⁷. In general, the $\Delta R/R$ for at least two locations (45° in addition to 0° or 90°) is successfully captured and is plotted in Fig. 12. This figure indicates that there exists minimal deformation observed at 45° away from the valley (P45), which is a characteristic signature of dominant mode-2 vibration [33].

It can be seen in Fig. 12(a) and (b) that the 70% and 76% P_c experiments undergo only mode-2 stable vibrations and no implosion is observed. The peak amplitude of the vibration ($\Delta R_{\max}/R$) at the valley is found to be ~ 5.0% and 7.1% for 70% and 76% P_c experiments respectively. Interestingly in both of these cases, the implodable volumes are vibrating beyond 3.8% $\Delta R/R$, which is the critical deformation of the implodable volume at the collapse pressure (see Sec 3.2.1), but the hydrostatic pre-pressure in the confining tube is not sufficient to make structure unstable. Thus, it validates the minimal deformation limit hypothesis mentioned in Sec 3.2.1. Structures can be excited beyond the critical deformation at P_c , but the level of hydrostatic pressure governs whether this excitation will cause instability or not. In Geo A, it requires minimum 12% to 14% $\Delta R/R$ excitation in order to reach the

⁷ The curved geometry of the implodable volume limits to around 100° and 120° of the circumferential DIC data in Geo A and Geo B experiments respectively.

unstable regime at 76% and 70% P_c respectively (from Fig. 8(d)); hence these observed vibrations don't lead to implosion in these cases. Looking at the “noisy” vibrations present at P45, it can be seen that it is vibrating at relatively higher frequencies indicating the presence of higher order vibrations modes. The close inspection on the Fig. 12 (a) and (b) also shows that the mean position of vibration for P0 shifts below zero indicating that the implodable volume should have incurred permanent irrecoverable plastic deformation shifting the mean position of vibration.

Figure 12 Peak deformations observed from DIC measurement in the Geo A implodable volume at varying pre-pressures. The graphs have been shifted in y-direction for clarity. (a) 70% P_c , (b) 76% P_c , (c) 80% P_c , (d) 85% P_c , (e) 90% P_c

The deformation history observed at higher pre-pressure (80% P_c , 85% P_c and 90% P_c) is shown in Fig. 12 (c) to (e). In these cases, the deformation at the lobe (P90) along with 45° location (P45) is plotted. The unstable growth of $\Delta R/R$ in later time history suggests that the strength of the shock wave coupled with higher pre-pressure is strong enough to initiate the implosion. Although all these cases are similar in

nature, the 80% P_c and 85% P_c experiments indicate a delay of $\sim 2.5 - 3.0$ ms between the instability initiation and the shock wave loading on the implodable volume, while 90% P_c leads to direct implosion. During this delay period, the implodable volume exhibits initial radial symmetric excitation followed by half/one cycle of mode-2 vibration. It appears that in these two cases, the initial shock wave loading is not strong enough to make structure unstable, but the subsequent loading from bubble pulse/reflections aids in providing additional impulse to pass the stability point.

The results for Geo B shock initiated experiments are shown in Fig. 13. It is seen that the 79% and 84% P_c experiments exhibit the mode-2 structural vibrations shown by out-of-phase oscillating lobe (P90) and valley (P0). Similar to Geo A, Geo B also shows that the mean position of vibration for P0 shifts below zero and it shifts above zero for P90 indicating a permanent plastic deformation occurred in the implodable volume. The maximum amplitude of vibrations is found to be $\sim 2.8\%$ and 3.4% for 79% P_c and 85% P_c experiments. These amplitudes are much lower than the theoretical threshold shown in Fig. 8(d), hence stable vibrations are observed. The further increase in pre-pressure to 90% P_c leads to a “delayed” collapse of implodable volume similar to seen in Geo A (see Fig. 13(c)). During this delayed time interval, the increasing magnitude of deformation is later accompanied by a minor decrease. But later, the monotonic increase in deformation shows the unstable collapse of the implodable volume. This monotonic increasing is also a result of subsequent secondary pressure loading shown earlier in Sec. 3.3. The highest pre-pressure of 95% P_c shows direct collapse as shown in Fig. 13(d).

Figure 13 Peak deformations observed from DIC measurement in the Geo B implodable volume at varying pre-pressures. The graphs have been shifted in y-direction for clarity. (a) 79% P_c , (b) 84% P_c , (c) 90% P_c , (d) 94% P_c

3.5 Frequency Analysis of Vibration Response

3.5.1 Experimental Frequency

The vibration frequency of the implodable volume during a shock initiated experiments shows the characteristic vibration frequency of a certain mode shape. As all the experiments conducted in this study indicate dominant mode-2 vibrations, the frequency analysis is conducted using fast Fourier transform (FFT) of the deformation data. For simplicity, DIC radial deformation data is obtained at the maximum deformation longitudinal location (as shown in schematic in Fig. 12) and the results of FFT analysis are shown in Fig. 14. This figure shows that GeoA exhibits a peak at ~ 750 Hz, while GeoB exhibits a peak at ~ 400 Hz. These peaks disappear in both Fig.

12(a) and (b) at intermediate 45° locations confirming mode-2 shape for these vibration frequencies.

Figure 14 Fast Fourier transform of the deformation data (a) Geo A at 70% P_c , (b) Geo B at 79% P_c

3.5.2 Analytical Evaluation of Uncoupled Vibration Frequencies

The effect of initial pre-stress on the vibration frequencies has been analytically evaluated by several researchers in past [34–36]. The most general solution of a simply supported cylindrical shell for small circumferential mode numbers (which is $n = 2$ in this study) has been calculated by Armenakas [35], which can be written as [35]:

$$\left(\frac{\omega}{\omega_s}\right)^2 = \frac{2n^2 h^2}{(1-\nu)\pi^2 R^2} \left[\frac{(1-\nu^2)\beta^4}{n^2+1} + \frac{s^2(n^2-1)^2}{3(n^2+1)} + \frac{n^2-1}{n^2+1} \bar{N} + \beta^2 \bar{T} \right] \quad (1)$$

$$\text{where } \omega_s = \frac{\pi}{h} \left(\frac{G}{\rho} \right)^{1/2}; \quad \beta = \frac{\pi R m}{L n}; \quad s = \frac{h}{2R}; \quad E_p = \frac{E h}{(1-\nu^2)}; \quad \bar{N} = \frac{N}{E_p}; \quad \bar{T} = \frac{T}{E_p};$$

$$N = PR; \quad T = \frac{PR}{2}$$

The first term represents the membrane effect, second term, the effect of bending, and the last two terms are the effect of initial stress. This equation can be further simplified by using the definition of critical collapse pressure and the frequency at zero pressure (ω_0). Using Eq. (1), the critical collapse pressure and ω_0 can be calculated by putting $\omega = 0$, which indicates buckling of the cylindrical shell,

$$\begin{aligned} \frac{(1-\nu^2)\beta^4}{n^2+1} + \frac{s^2(n^2-1)^2}{3(n^2+1)} + \frac{n^2-1}{n^2+1}P_cR + \beta^2\frac{P_cR}{2} &= 0 \\ \left(\frac{\omega_0}{\omega_s}\right)^2 &= \frac{2n^2h^2}{(1-\nu)\pi^2R^2} \left[\frac{(1-\nu^2)\beta^4}{n^2+1} + \frac{s^2(n^2-1)^2}{3(n^2+1)} \right] \end{aligned} \quad (2)$$

By further simplification, the ratio of natural frequency at hydrostatic pressure (ω) and ω_0 the natural frequency at zero pressure can be written as,

$$\left(\frac{\omega}{\omega_0}\right)^2 = 1 - \frac{P}{P_c} \quad (3)$$

The plot of ω/ω_0 as a function of P/P_c inside the confining tube is shown in Fig. 13. It must be noted that the Eq.(3) does not account for the effect of mass of water, hence these frequency solutions are valid for the pre-stress generated by the gaseous medium (negligible mass). Thus, the frequencies estimated by Eq. (3) are higher than experimentally observed frequencies. The main reason behind such slower vibration frequencies in the experiments is the coupling of the implodable volume with the surrounding fluid. The fluid present between the implodable volume and the

confining tube undergoes acceleration during vibrations and it results into inertial forces in the fluid [23] resulting into slower vibration frequency.

It can also be seen that the decrease in frequency shown in Eq. (3) is only a function of pre hydrostatic pressure applied and doesn't have a geometry parameter explicitly, therefore any cylindrical shell, both Geo A and Geo B in this study, will exhibit similar change in frequency with increasing pre-pressure (P/P_c). The plot in Fig. 15 shows that the natural frequency decreases significantly at higher pre-pressures and at $\sim 70\% P_c$ (minimum pre-pressure used in this study), it decreases to about $54\% \omega_0$. Thus, for example, the natural frequency for Geo A at zero pre-pressure, which is 2408 Hz, will decrease to 1319 Hz at $70\% P_c$.

Figure 15 Change in natural frequency as a function of applied hydrostatic pre-pressure on the implodable volume

A further term accounting for the added mass of the surrounding fluid can be added in Eq. (3) using [23] and the final analytical frequency with the effect of initial stress and the effect of water can be written as;

$$\left(\frac{\omega}{\omega_0}\right)^2 = \left(1 - \frac{P}{P_c}\right) \times \left(\frac{1}{1+M}\right) \quad (4)$$

where $M = \frac{2}{5} \left[\frac{\rho_w a}{\rho h} \left(\frac{(b/a)^4 + 1}{(b/a)^4 - 1} \right) \right]$ represents the added mass factor from [23];

b = Inner radius of the confining tube; a = Outer radius of the implodable volume; h = Wall thickness of the implodable volume; ρ_w = density of water; ρ = density of aluminum. The comparison of analytically evaluated frequency from the Eq. (4) and experimentally observed frequency is shown in Table 3. It is seen that after the consideration of initial stress term and the effect of water, the frequency of oscillation can be predicted reasonably well within 15% error.

Geometry	Mode Shape	Free Structural Frequency (ω_0)	Experimental Frequency (ω_e)	Analytical Frequency (ω) from Eq. (4)	% Difference $\left \frac{\omega_e - \omega_0}{\omega_e} \times 100 \right $
GeoA	Mode-2	2408 Hz	740 Hz	682 Hz	7.8%
GeoB	Mode-2	1500 Hz	400 Hz	343 Hz	14%

Table 3 Comparison between the experimentally and analytically evaluated frequencies

4. Conclusions

An experimental study was conducted to understand the shock initiated implosion mechanics inside a confining environment. The key findings of this study are as follows:

- (1) At relatively lower hydrostatic pre-pressures, the implodable volume undergo forced vibration under shock wave loading, which may result into plastic deformation in the implodable volume. Hence, an exposure to shock wave loading may damage the structure and lower its critical strength.

- (2) The shock wave imparted from the longitudinal direction results into generation of higher order longitudinal axisymmetric modes as the shock wave impact the implodable volume. These modes slowly disappear after $\sim 300\text{-}400\ \mu\text{s}$ and the energy of these modes transfers to the lowest pressure mode shape (mode-2 in this study).
- (3) The vibration frequency in the underwater shock initiated experiments with hydrostatic pre-pressures is significantly lower than the free structural vibration frequency. The primary reason for such decrease is due to the effect of pre-stress on the implodable volume imposed by the hydrostatic water pressure, along with the added mass effect of water present between the implodable volume and the confining tube.
- (4) The calibration using a submerged calibration grid can successfully account for the refractive index mismatch between the water/acrylic window/air. The calibration experiments reveal that the both the in-plane and out-of-plane measurements can be measured using this modified 3-D DIC calibration procedure within 4% error.

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CHAPTER 9

CONCLUSIONS

A fundamental experimental investigation was conducted to understand the underwater dynamic implosion of cylindrical metallic shells. In particular, the attention was focused on studying the generation of pressure waves from implosion in varying confining situations such that the fluid structure interaction with the confined structures governs the mechanics of implosion process. The experiments were conducted in three environments: (a) free-field environment represented by a large underwater pressure vessel facility, (b) partially closed environment represented by one-end open confining tube, (c) completely closed environment represented by closed confining tube. This study also utilizes the visualization and understanding of the real-time deformation of the implodable volumes using high speed 3-D Digital Image Correlation (DIC) technique. The major findings of this study are as follows:

- 3D DIC technique can be successfully utilized for displacement measurements for submerged objects by extracting intrinsic and extrinsic parameters using a submerged calibration grid given that the camera's axes are aligned with the surface normal of the window. The error of measured DIC displacement is < 5% in general for both out-of-plane and in-plane motion.
- There exists a large degree of pre-deformation (up to 10% of the radius2 mm) in the cylindrical shells prior to any dynamic pressure fluctuations in the near-field during a free-field experiment. This pre-deformation varies linearly along

the implodable length and occurs relatively slowly over the entire length of the implodable.

- After the initiation of the point contact during implosion, the contact front moves with very high velocities (~ 350 m/s to 600 m/s) before reaching a stable buckle propagation velocity between ~ 100 m/s and 200 m/s in free-field experiments.
- The rate of growth in contact area during a free-field implosion has significant influence on the generation of a high pressure spike. The high rate of increase in contact area leads to large changes in the momentum of water following the walls and this causes a large spike in the pressure.
- The implosion of the implodable volume (collapse pressure = P_c) inside a one-end open confining tube leads to strong oscillating water hammer waves with $1.35P_c$ to $1.92P_c$ magnitude. Study also reveals that the varying collapse pressure leads to faster implosions in one-end open confining tube. Both peak and average structural velocities increase linearly with increasing collapse pressure.
- The effect of confining environment is most promptly seen in relatively lower collapse pressure one-end open implosion experiments which indicate a long deceleration phase following the peak velocity until wall contact initiates. The duration of this deceleration phase decreases with increasing collapse pressure.
- Sympathetic implosion experiments show that implosion of the weaker implodable volume (critical pressure = P_c) inside the confining tube leads to the subsequent sympathetic implosion of the stronger implodable volume

(critical pressure $= 1.2P_c$). Implosion of the weaker implodable volume produces strong oscillating water hammer waves with $1.6P_c$ peak over-pressure, which initiates the implosion of the stronger implodable volume.

- In a closed tube confined environment, the limited hydrostatic potential energy available significantly influences the implosion process. The wall velocities of the implodable volume during the collapse, as well as the extent of the collapse progression, are largely affected by the sudden decrease in the available hydrostatic potential energy.
- The normalized extent of collapse (i.e. $\Delta V_{implodable} / V_{implodable_i}$) is larger for the small diameter implodable compared to larger implodable volumes in confined closed tube implosion event. The change in volume of the implodable ($\Delta V_{implodable}$) is approximately 80-90% of the excess compressible water present inside the confining tube.
- The experiments revealed that the longitudinal asymmetric placement of the implodable volume enhances the strength of hammer pressure waves generated during the implosion process. The off-centered location of the implodable volume causes a pressure imbalance in the entire length of the confining tube.
- The presence of air bubbles inside the confining tube significantly affects the compliance of the system leading to slower waves propagating inside the confining tube. The cavitation occurring at the end-plates is inhibited by introducing air bubbles, leading to suppression of cavitation shock-wave damage at the end-plates.

- DIC full field deformation and velocity results show that the confined nature of the closed tube confining environment significantly alters the velocity of the implodable volume during collapse. The implodable volume accelerates in the initial collapse phase, but reaches its peak velocity well before the wall contact initiation or the maximum radial deformation due to large near-field pressure drop due to collapse of implodable volume. A deceleration in the implodable volume is also observed suggesting the presence of strong fluid-structure interaction between water and implodable. An addition of air bubble inside the confining tube resulted in a further increase in the collapse velocity of implodable volume and this effect is observed to further increment with increasing air bubble sizes.
- Shock initiated implosion study shows that there are four possible situations that can occur in shock initiated implosion problems depending upon level of pre-pressure: 1) lowest pre-pressure → elastic vibrations with no plastic deformation, 2) elasto-plastic vibrations with residual plastic deformation, 3) elasto-plastic vibrations accompanied by a delayed collapse of the implodable volume from the secondary pressure wave and 4) direct collapse of implodable volume from the shock wave.
- Shock initiated implosion experiments indicates that the vibration frequency is significantly lower than the free flexural vibration frequencies and found to reduce by a factor upto 3.3 times in these experiments. These lower frequencies are primarily caused by two effects: 1) the effect of initial pre-stress in the implodable volume due to the presence of hydrostatic pre-pressure

and, 2) the added mass of water surrounding the implodable volume. Further analytical consideration of these two factors result allows great agreement between experimental and analytically calculated frequencies.

APPENDICES

APPENDIX A: Fabrication of Closed Confining Tube Implosion Facility

The closed confining tube facility was fabricated at the University of Rhode Island's machine shop. Major portion of this fabrication was conducted with the guidance and help of **Mr. Joe Gomez** at mechanical engineering machine shop. The facility was designed in a modular fashion for ease of fabrication. The tube material chosen for the facility was **SA 106 Grade-B** steel recommended by the guidelines of pressure vessel handbook. This specific grade allows for a *seamless construction* of the confining tube. This grade has a minimum yield strength of 52000 psi (358 MPa). The tube material was purchased from Federal Steel Supply Inc.

The pipe size purchased was 8 in – Schedule 140. This specification leads to an inner diameter of 7.0 in for the confining tube with a wall thickness of 0.8125 in (13/16 in). Four sections were purchased during the fabrication: a) 2 sections of 36 in length, b) 1 section of 12 in length, and c) 1 section of 18 in length. Fabrication of the individual section along with more detailed drawings and pictures are discussed below:

1. Fabrication of 36 in and 12 in Sections

After receiving the tube sections from Federal Steel Supply Inc. in their exact final length, the inner surface was cleaned with a metallic scrubber to remove the inside rust. Two layer of heavy rust Rust-oleum metal primer was applied at the inner

surface. It was further coated with two layers of Rust-oleum black flat paint for corrosion resistance. The fabrication procedure for 36 in section and 12 in section was relatively similar, hence only the description for 36 in section is primarily covered here.

The Solidworks drawing of the 36 in section is shown in Fig. A1.

Figure A1 Solidworks drawing of 36 in section (threads are not shown)

The machining of the outer surface along with threading the outer diameter (male threads) was performed at lathe machine. As turning the outer surface requires a steady rest, which should not interfere with the outer surface, a custom made conical steady rest made of aluminum 6061-T6 was fabricated as shown in Fig. A2.

Figure A2 Solidworks drawing of the steady rest

The weight of the 36 in section was ~ 250 lbs, much higher for being handled by hands. Therefore, the section was tied using straps to a crane in the machine shop and was lifted/guided in the lather bench. A real image of the 36 in section on the lathe is shown in the Fig. A3. One side of the section was mounted on the lathe chuck and the custom steady rest was placed on the other end of the section. The chamfer section of the custom steady rest allowed aligning the inner diameter of the section with the axis of the steady rest. The other end of the custom steady rest was grooved such that it is mounted on the tail-stock of the lathe. By applying force from the tailstock end, the section was clamped against the custom steady rest.

It must be noted that the support provided by the custom steady rest was sufficient enough for turning the outer diameter without chattering. After successful turning of the outer diameter, the male threads at the outer diameter were made. The threads were designed according to specifications provided by machinery handbook. For 8 in nominal diameter, the handbook suggests 8 threads/inch for threads. Thus, the

pitch size (P) = 0.125 in. After turning down the outer diameter, it was decided that the major diameter (D_{maj}) for the threads would be 8.450 in.

- Using formula for pitch diameter (D_p) = $D_{maj} - (0.64952 \times P) = 8.369$ in
- Using formula for minimum diameter (D_{min}) = $D_{maj} - (1.082532 \times P) = 8.315$ in

Figure A3 Real images of 36 in section along with custom steady rest

The 2 in length at each end of the pipe was further turned down to the D_{maj} for the purpose of threading. During grooving the male threads on the pipe, it was realized that the custom steady rest did not provide enough rigidity in order to machine threads on the lathe. Significant degree of chattering was present during threading, which would end up making poor quality threads. Thus, a three-point lathe steady rest was placed for support instead of the custom made steady rest (see Fig. A4). This three-point lathe steady rest allowed three points on the tube to be constantly supported throughout the rotation. After its installation, no chattering was observed

during threading indicating a greater support/rigidity provided by three-point steady rest. After finishing the threading, the o-ring groove was made on the each end of the pipe in order to provide sealing while joining them together in modular fashion. The o-ring groove was designed for 367 no. parker o-ring (ring OD = 7.895 in, width = 0.210 in).

Figure A4 Three-point steady rest used for fabrication of male threads

After threading one side of the section, it was again lifted by the crane. After flipping the orientation, it was again mounted on the lathe for threading the other side of the section. Note that at this point, the lathe chuck would hold the pipe on the outer surface of the threads, which is not a flat surface. If a large clamping force was provided on the lathe chuck, the jaws may destroy the threads of the pipe. In order to avoid any of these difficulties, copper shims were placed between the jaws and the threads in order to distribute the load and to avoid direct contact with the threads. Due to the soft nature of copper, clamping jaws of lathe chuck distorted the copper shims leading to no damage in the threads and better clamping was achieved. A picture of the copper shims near the lathe chuck jaws is shown in Fig. A5.

Figure A5 Copper shims at the lathe chuck jaws

2. Fabrication of Central Window Section (18 in section)

The initial fabrication process for turning and threading for central window section is similar to the previous 36 in section. The additional fabrication was performed in this section by removing the central portion and welding additional parts in order to incorporate an acrylic window. The solidworks drawing of the section is shown in Fig. A6.

Figure A6 Solidworks drawing of the window section

The central 9.0 in x 4.5 in section was removed by profile cutting in the CNC milling machine. Four more parts were also fabricated as shown in Fig. A7 and A8.

Figure A7 First additional part of the window section assembly

Figure A8 Second additional part of the window section assembly

These parts were machined at CNC milling machine. These parts can be assembled together on the 18 in window section in on order to provide a flat support for mounting a 2.5 in thick flat acrylic window. After the fabrication of these parts, they were welded together as shown in Fig. A9. Welding was performed at While Metal Welding, Charlestown, RI.

Figure A9 Real Images during the welding of the window section parts

The final assembled solidworks drawing of the window section is shown in Fig. A10.

Figure A10 Solidworks drawing for the assembled window section

3. Fabrication of Threaded Flanges and the End-plates

The flanges and the end-plates are also the essential part of the confining tube facility. Both flanges and end-plate were made out of 1018 steel. The detailed drawing of flange and the end-plate is shown in Fig. A11 and A12.

Figure A11 Solidworks drawing of the flange

Figure A12 Solidworks drawing of the end-plate

Both flange and the end-plate consist of 12 through holes, which can be used to join the modular sections of the confining tube and can also be used to close the end using end-plate. The schematic of the holes for both flanges and end-plate is exactly same.

The flanges were also threaded in similar manner as of the male threads. After cutting the central portion out from the plate on CNC milling machine, a boring tool was used to precisely achieve the minor diameter of the threads on lathe machine. This was followed by cutting the female threads on the flange. Please note that the female threads of the flange were fabricated prior to the fabrication of the male threads on the confining tube. Later, the male threads were machined in order to match the groove depth of the flange female threads. This process makes unique combination between a

flange and a corresponding male thread. Hence, a fabricated flange/male pair will only fit with each other (not with any other threads) in general. For ease, the matching male threads on the pipe and the flange were numbered for pairing purposes.

4. Vessel Supports and Clamps

In order to mount the confining tube over an I-beam section, several vessel supports were manufactured. These supports were also used in order to clamp the vessel on a milling machine to drill/tap the sensor holes/counter-bores. These supports were made out of aluminum 6061-T6. The solidworks drawing of both curved supports (top and bottom) and the bottom flat support are shown in Fig. A13 to A15.

Figure A13 Solidworks drawing of the bottom curved support

Figure A14 Solidworks drawing of the top curved support

Figure A15 Solidworks drawing of the bottom curved support

5. Maximum Design Pressure of Pressure Vessel

According to the pressure vessel handbook, vessel rating can be calculated using following formula:

$$P = \frac{2SEt}{D+1.2t}$$

where P = maximum allowable hydrostatic pressure

S = maximum stress for SA-106 B steel (15000 psi)

E = joint efficiency (for seamless pipe, $E = 1$)

t = minimum wall thickness of the pipe (0.33 in at pressure sensor location)

D = inner diameter of the pipe (7.0 in)

Thus,

$$P = \frac{2 \times 15000 \times 1 \times 0.33}{7 + (1.2 \times 0.33)} = 1339 \text{ psi}$$

Please note that this rating is not valid, when “window section” is installed in the facility. The window section rating mainly depends upon the quality/thickness of the acrylic window. The static numerical simulations on 2.5 in thick acrylic window show that the acrylic window will not reach yield point upto 1000 psi of hydrostatic pressure loading. But it must be noted that the flat design of the window leads to stress concentrations at the steel bracket during operation. Also the brittle nature of acrylic window makes it highly susceptible to cracking during experiments. These initial cracks can be formed during fabrication/mishandling of the window. Thus, it is always advised to closely inspect the window prior to every series of experiments. The current window present at the time of preparing this dissertation was successfully tested up to 600 psi hydrostatic pressure loading.

6. Installation of Pressure Sensors

The pressure sensors used in this facility were dynamic pressure transducers purchased from PCB Piezotronics, Inc. (PCB-113B22). The measurement range, sensor is calibrated for, was 0 – 5000 psi. The maximum pressure rating on the sensor was 15000 psi. The nominal sensor sensitivity was 1 mV/psi. The sensors have very small rise time ($< 1 \mu\text{s}$), which make them suitable for capturing dynamic change in pressure especially the shock wave. The sensors have high resonant frequency ($> 500 \text{ KHz}$). The full technical specifications of this sensor from <http://www.pcb.com/> can be seen in Fig. A16. The drawing of the sensor is shown in Fig. A17 reproduced from <http://www.pcb.com/>.

These sensors were installed in the confining tube such that the sensor element was flush mounted on the inner wall of the confining tube. As the direct installation of the sensor on the confining tube was difficult, a pipe thread adapter was installed on each sensor. This allows the sensors to be installed directly on the confining tube using female taper pipe threads. The procedure to prepare mounting hole provided by <http://www.pcb.com/> is shown in Fig. A18.

Model Number 113B22		ICP® PRESSURE SENSOR		Revision: C ECN #: 31258	
Performance		ENGLISH	SI	OPTIONAL VERSIONS	
Measurement Range(for ±5V output)		5 kpsi	34,475 kPa	Optional versions have identical specifications and accessories as listed for the standard model except where noted below. More than one option may be used.	
Useful Overrange(for ± 10V output)		10 kpsi	68,950 kPa		
Sensitivity(± 10 %)		1.0 mV/psi	0.145 mV/kPa	E - Emralon coating [5]	
Maximum Pressure		15 kpsi	103,425 kPa		
Resolution		20 mpsi	0.14 kPa	Coating Emralon Emralon	
Resonant Frequency		≥ 500 kHz	≥ 500 kHz	Electrical Isolation 10 ⁸ ohm 10 ⁸ ohm	
Rise Time		≤ 1.0 μ sec	≤ 1.0 μ sec	Supplied Accessory : Model 065A08 Isolation ring 0.250"OD x 0.218" ID x 0.027" thk anodized aluminum (3)	
Low Frequency Response(-5 %)		0.001 Hz	0.001 Hz	Supplied Accessory : Model 065A22 Isolation Seal, .250" OD x .218" ID x .015", Torlon or Vespel (3)	
Non-Linearity		≤ 1.0 % FS	≤ 1.0 % FS	[3]	
Environmental				H - Hermetic Seal [5]	
Acceleration Sensitivity		≤ 0.002 psi/g	≤ 0.0014 kPa/(m/s ²)	Sealing Welded Hermetic Welded Hermetic	
Temperature Range(Operating)		-100 to +275 °F	-73 to +135 °C	[1]	
Temperature Coefficient of Sensitivity		≤ 0.03 %/°F	≤ 0.054 %/°C	J - Ground Isolated [5][6]	
Maximum Flash Temperature		3000 °F	1649 °C	N - Negative Output Polarity [5]	
Maximum Vibration		2000 g pk	19,614 m/s ² pk	S - Stainless Steel Diaphragm [5]	
Maximum Shock		20,000 g pk	196,140 m/s ² pk	Diaphragm 316L Stainless Steel 316L Stainless Steel	
Electrical				W - Water Resistant Cable [7][5]	
Output Polarity(Positive Pressure)		Positive	Positive	Supplied Accessory : Model 060A03 Clamp nut, 5/16-24-2A thd, 1/4" hex, stainless steel (1)	
Discharge Time Constant(at room temp)		≥ 500 sec	≥ 500 sec	WM - Water Resistant Cable [7][5]	
Excitation Voltage		20 to 30 VDC	20 to 30 VDC	Supplied Accessory : Model 060A05 Clamp nut M7 x 0.75-6g thd (1)	
Constant Current Excitation		2 to 20 mA	2 to 20 mA	NOTES:	
Output Impedance		<100 ohm	<100 ohm	[1] Typical.	
Output Bias Voltage		8 to 14 VDC	8 to 14 VDC	[2] For +10 volt output, minimum 24 VDC supply voltage required. Negative 10 volt output may be limited by output bias.	
Physical				[3] Zero-based, least-squares, straight line method.	
Sensing Geometry		Compression	Compression	[4] See PCB Declaration of Conformance PS023 for details.	
Sensing Element		Quartz	Quartz	[5] For sensor mounted in thread adaptor, see adaptor installation drawing for supplied accessories.	
Housing Material		17-4 Stainless Steel	17-4 Stainless Steel	[6] Used with optional mounting adaptor.	
Diaphragm		Invar	Invar	[7] Clamp nut installed prior to cable attachment	
Sealing		Welded Hermetic	Welded Hermetic	SUPPLIED ACCESSORIES:	
Electrical Connector		10-32 Coaxial Jack	10-32 Coaxial Jack	Model 060A03 Clamp nut, 5/16-24-2A thd, 1/4" hex, stainless steel (1)	
Weight		0.21 oz	6.0 gm	Model 060A05 Clamp nut M7 x 0.75-6g thd (1)
[4]	Model 065A02 Seal ring, sensor flush mount, 0.248" OD x 0.219" ID x 0.015" thk, brass (3)		
			Model 065A05 Seal sleeve sensor recess mount 0.248" OD x 0.221" ID x 0.240" thk 17-7 (1)		
All specifications are at room temperature unless otherwise specified. In the interest of constant product improvement, we reserve the right to change specifications without notice. ICP® is a registered trademark of PCB Group, Inc.					
Entered: <i>SLH</i>		Engineer: <i>WJL</i>		Sales: <i>RWM</i>	Approved: <i>CLZ</i>
Date: <i>9-9-09</i>		Date: <i>8-25-09</i>		Date: <i>9-1-09</i>	Date: <i>9-2-09</i>
					40643
Phone: 716-684-0001 Fax: 716-686-9129 E-Mail: pressure@pcb.com 3425 Walden Avenue, Depew, NY 14043					

Figure A16 Technical specifications of PCB-113B22 pressure sensors (see <http://www.pcb.com/> for details)

Figure A17 Detailed drawing of the PCB-113B22 pressure sensors (see <http://www.pcb.com/> for details)

Figure A18 Detailed drawing of the pipe thread adapter installation (see <http://www.pcb.com/> for details)

7. Full Assembly of Closed Confining Tube Implosion Facility

The final solidworks drawings of the two configurations (with window/without window) of the closed confining tube facility are shown in Fig. A19 and A20.

Figure A19 Solidworks drawing of the full assembly without window

Figure A20 Solidworks drawing of the full assembly with window

APPENDIX B: Fabrication of Open-Ended Confining Tube Facility

One end open confining tube facility was also fabricated at the University of Rhode Island's machine shop. The facility was designed in a modular fashion for ease of fabrication. The tube material chosen for open tube facility was aluminum 6061-T6. The inner diameter of the confining tube was 7.0 in with 1.0 in wall thickness. The drawings for individual parts of the facility are discussed below:

1. Tube Section

The solidworks drawing for the tube section is shown in Fig. B1. The material was purchased from F. W. Webb Company, Cranston, RI.

Figure B1 Solidworks drawing of the aluminum tube section (holes represent the location of the pressure sensors)

2. End-plate and Flange

The end-plate and flange for open tube facility were made out of 2 in thick aluminum plate. The solidworks drawings for the end-plate and the flange are shown in Fig. B2 and B3.

Figure B2 Solidworks drawing of the end plate

Figure B3 Solidworks drawing of the flange

3. Acrylic Curved Window Section

The solidworks drawing for the curved window section is shown in Fig. B4.

The acrylic section was purchased from San Diego Plastics.

Figure B4 Solidworks drawing of the curved window section

4. Final Assembly of the Open Ended Confining Tube

The table for mounting the open ended confining tube facility is assembled inside the 84 in inner diameter underwater pressure vessel facility at URI. The table is primarily made out of aluminum T-slotted structural framings. After assembling the truss structure of the table, 1 in thick aluminum base-plate of size 12 in x 24 in is secured on the truss structure. The confining tube facility is later assembled on this base-plate. Each end of the aluminum section is grooved with an o-ring for sealing purposes. The smaller diameter groove is for o-ring no. 264 and larger diameter groove is for o-ring no. 268.

For compressing each section of the open tube together, four 5/8" diameter threaded rods are passed through between the flange and the end-plate. These rods penetrate beyond the base-plate and the flange nuts are placed on the base-plate below the end-plate and on the flange. By tightening these bolts, the assembly undergoes compression causing o-rings to seal the individual section of the confining tube. Full assembly of the open ended confining tube facility with window section is shown in Fig. B5.

Figure B5 Solidworks assembly drawing of the open ended confining tube facility

A real picture of the assembled confining tube facility without the acrylic window section is shown in Fig. B6.

Figure B6 Real picture of the open ended confining tube facility without window

APPENDIX C: More Details on Chapter-2

1. Minimum/maximum ($P_{\min}$ / P_{peak}) pressure

The minimum pressure observed during the pressure trough (under-pressure region prior to wall contact) in the implosion for all the specimens as a function of sensor location is plotted in figures C1(a) and C1(b). It is clear that $P_{\min}$ at 0° is slightly larger than $P_{\min}$ at 90° ; and the difference is ~ 0.05 MPa to 0.1 MPa depending upon Z / L location. This is due to the presence of high inward water velocity along the 0° direction causing more change in dynamic pressure than that is experienced at 90° . Although there is a disparity in the critical buckling pressure between the specimens, the value of $P_{\min}$ is found to be ~ 0.5 MPa at $Z = 0$ for all 0° sensors. This suggests that the under-pressure phase is a highly local phenomenon and is mainly governed by ‘local wall velocity / deformation behaviours’ of the specimen. As the velocity history at the centre for all $2L/D$ ratios is similar, $P_{\min}$ also exhibits similar values.

When investigating the ratio $P_{\min}/P_c$ (see figure C1(b)), it is seen that the ratio is large (~ 0.27) for small P_c (large $2L/D$) at $Z / L = 0$. This effect diminishes as the sensor location moves away from the centre and the ratio converges to an approximately constant $P_{\min}/P_c = 0.13$ at $Z / L = 1$. This indicates that the magnitude of under-pressure waves released during stable buckle propagation is $\sim 0.13P_c$ for the implodable volumes used in this study. This is an important observation which can be extended for long submerged pipelines. As the magnitude of under-pressure wave is $0.13P_c$ for the outer diameter and wall thickness chosen, a long submerged pipeline of similar dimensions can be expected to generate a $0.13P_c$ magnitude under-pressure

pulse if the pipeline is manufactured from the same material. Hence, if buckles in a long submerged pipeline move with a characteristic velocity depending upon the geometry (Kyriakides and Netto, 2000), then the under-pressure pulse released during the stable buckle propagation in long submarine pipelines can also be thought of as a ‘characteristic pressure pulse’.

Figure C1 Analysis of measured minimum pressure and the largest pressure spike for all the specimens (a) Plot of minimum pressure (P_{min}) versus Z/L (b) Plot of normalized minimum pressure (P_{min}/P_c) versus Z/L (c) Plot of largest pressure spike (P_{peak}) versus radial distance

To examine the decay of high pressure waves generated in the implosion process, the peak amplitude observed for large pressure spike (P_{peak}) is plotted as a function of spherical radial distance from the centre of the implodable in figure C1(c) (only the 0° sensor is discussed here, as it exhibited a distinct pressure spike). It can be

seen that the large pressure spike decays almost linearly with radial distance.

Interestingly, the magnitude of the large pressure spike increases with 2L/D ratio.

Hence, longer specimens exhibit larger pressure spikes.

2. Energy

The acoustic energy ($E_{acoustic}$) released during the positive pressure phase of an implosion process can be expressed as:

$$E_{acoustic} = 4\pi r_{sen}^2 \int_0^{T^+} \frac{P(t)^2}{2\rho c_0} dt \quad (1)$$

where r_{sen} = radial distance between the centre of implodable volume (at axis of cylindrical implodable volume, $Z = 0$) after collapse and the sensor location.

From the underwater hydrodynamic relations, Cole (Cole, 1948) has shown that for a spherical wave, the velocity of fluid at any time $t = T_0$ is a function not only of the pressure at that time but also the changes in pressure that occurred before $t = T_0$. Hence, the fluid has an outward velocity, even after the passage of spherically outward moving shock wave (also referred as afterflow (Cole, 1948)). This implies that the fluid velocity will be directly proportional to the area under the pressure-time history (or impulse), which leads to an extra term in the energy expression (Arons and Yennie, 1948) as shown in Eq. (2).

$$E_{total} = E_{acoustic} + E_{afterflow} = 4\pi r_{sen}^2 \left[\int_0^{T^+} \frac{P(t)^2}{2\rho c_0} dt + \frac{1}{2\rho r_{sen}} \left(\int_0^{T^+} P(t) dt \right)^2 \right] \quad (2)$$

In the case of an implosion process, the duration of the large pressure spike emitted as a shock wave is very small ($\sim 100 \mu s$) and the impulse in this duration is

negligible. Hence the energy released in the large pressure spike mainly consists of acoustic energy, which is given by Eq. (1). After this time interval, long duration positive pressure waves are released during the buckle propagation process. Even though the amplitude of these waves is small (~ 0.25 MPa - 0.40 MPa) as compared to large pressure spike (~ 2 MPa), the long-time duration accumulates significant impulse leading to a large after-flow energy. Therefore, the energy released during an implosion process mostly consists of after-flow energy due to the spherical nature of the shock wave. These energies (both $E_{acoustic}$ and $E_{afterflow}$) were evaluated for all the experiments. For brevity, only the results for the central location sensor (M-0 and M-90) are shown in figure C2. Figure C2(a) shows that $E_{acoustic}$ is fairly small (< 5 J) and consists of less than 1.5% of the potential energy of the implodable volume (which is critical collapse pressure times the internal volume of the implodable (Orr and Schoenberg, 1976; Urick, 1963)). It is consistent with the observation made by Urick (Urick, 1963), who concluded that energy conversion from potential energy to acoustic energy is fairly small ($\sim 0.2\%$ to 0.6%). As the strength of the shock wave was stronger for larger $2L/D$ ratio, $E_{acoustic}$ slightly increases with increase in $2L/D$ ratio (from 3 J to 5 J).

From the plot of $E_{afterflow}$ given in figure C2(b), 5-6% of the potential energy is released as $E_{afterflow}$ during the implosion process. Similar to $E_{acoustic}$, $E_{afterflow}$ also exhibits a minor increase with an increase in $2L/D$ ratio (from 36 J to 40 J). In general, the total energy conversion from potential energy to acoustic and after-flow energy is very small (in total $< 7\%$), which remains similar for even large $2L/D$ ratio.

Figure C2 Evaluation of acoustic and afterflow energy released during implosion experiments (a) Plot of acoustic energy released ($E_{acoustic}$) for all specimens (b) Plot of afterflow energy released ($E_{afterflow}$) for all specimens

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