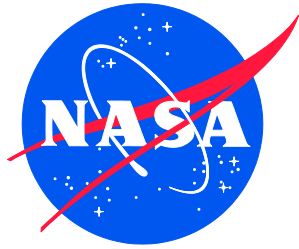


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A Compilation of Composite Overwrapped Pressure Vessel Research (2015–2021)

William Schonberg
Missouri University of Science and Technology, Rolla, Missouri

August 2023

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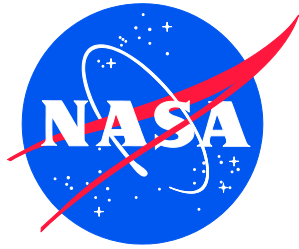
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National Aeronautics and
Space Administration

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Preface

Most spacecraft have at least one pressurized vessel on board. For robotic spacecraft, it is usually a liquid propellant tank. One of the design considerations of such spacecraft is the possible damage that might occur in the event of an on-orbit impact by a micrometeoroid or orbital debris (MMOD) particle. While considerable effort has been expended in the study of the response of non-pressurized spacecraft components to these kinds of impacts, relatively few studies have been conducted on the pressurized elements of such spacecraft.

This document presents the results of a series of studies performed to develop data-driven rupture limit equations (RLEs) and ballistic limit equations (BLEs) for composite overwrapped pressure vessels (COPVs). These equations can be used to differentiate between impact conditions that would result in only a small hole or crack from those that would cause catastrophic tank failure. This information would be useful in selecting tank materials to avoid catastrophic tank failure in the event of a perforating on-orbit MMOD particle impact.

The chapters in this document contain the following articles that have appeared in a variety of archival journals.

(1) Schonberg, W.P., “Rupture of a Cryogenic Composite Overwrapped Pressure Vessel Following a High-Speed Particle Impact”, *Aerospace*, Vol. 5, 2018, Paper No. 20.

This paper considers a small set of COPV rupture/non-rupture results in a first attempt at developing a method for determining an RLE for such data. It documents the results of an initial proof-of-concept effort.

(2) Schonberg, W.P., “Will it Leak? Will it Burst? COPV Perforation and Rupture after an MMOD Impact”, *Journal of Space Safety Engineering*, Vol. 7, No. 3, 2020, pp. 213-221.

This paper presents two statistics-based methods for developing an RLE for cylindrical COPVs to separate rupture and non-rupture test results. The first is a regression-based method, while the second is a discriminant line approach.

(3) Schonberg, W.P., “Predicting the Rupture of a COPV Impacted by a High-Speed Orbital Debris Particle: Including the Effects of Temperature and COPV Contents”, *ASCE Journal of Aerospace Engineering*, Vol. 34, No. 3, 2021, Paper No. 06021002.

This paper extends the cylindrical COPV RLE developed in paper (2) to include temperature (cryogenic as well as room temperature) and contents (cryogenic fluids as well as air and water).

(4) Schonberg, W.P., “Rupture Limit Equation for Shielded COPVs”, *International Journal of Pressure Vessels and Piping*, Vol. 190, 2021, Paper No. 104319.

This paper extends the cylindrical COPV RLE developed in paper (3) to include the effects of shielding.

(5) Schonberg, W.P., “Non-Catastrophic Perforation of a Composite Overwrapped Pressure Vessel”, *Acta Astronautica*, Vol. 174, 2020, pp. 41-47.

This paper develops a BLE for COPVs as well as empirical equations to predict the diameter of the holes in the composite overwrap and in the liner (in the event of a through-hole) at the initial impact site.

In all five studies, the equations developed were found to be fairly adept in predicting the rupture/non-rupture and perforation/non-perforation response of shielded and unshielded pressurized COPVs at room and cryogenic temperatures. Further testing is needed, particularly for impact conditions and internal pressures not yet considered and for other COPV configurations and constructions in order to further refine the RLEs and BLEs presented herein, and to render them applicable under a wider range of impact scenarios and operating conditions.

Table of Contents

1.0	Rupture of a Cryogenic Composite Overwrapped Pressure Vessel Following a High-Speed Particle Impact.....	1
1.1	Introduction.....	1
1.2	Data Overview	2
1.3	Rupture Limit Equation Development\.....	4
1.4	Comparison Against Empirical Results	8
1.5	Concluding Thoughts.....	9
1.6	Acknowledgements.....	9
1.7	References.....	10
2.0	Will it Leak? Will it Burst? COPV Perforation and Rupture after a MMOD Impact	11
2.1	Introduction.....	11
2.2	Testing Program Overview	13
2.2.1	Comments on the Data	14
2.3	Rupture Limit Equation Development.....	15
2.3.1	Regression Analysis Approach.....	17
2.3.2	Comparison of Regression-based RLE against Empirical R/NR Results	18
2.3.3	Discriminant Line Analysis Approach.....	22
2.3.4	Comparison of Discriminant-based RLE against Empirical R/NR Results	23
2.4	Ballistic Limit Equation Development	25
2.4.1	Comments on the Data	26
2.4.2	Equation Development	26
2.4.3	Comparison of the BLE against Empirical P/NP Results.....	26
2.5	Practical Considerations When Applying the COPV RLE and BLE.....	27
2.6	Conclusions.....	28
2.7	Acknowledgments.....	28
2.8	References.....	29
3.0	Predicting the Rupture of a COPV Impacted by a High-Speed Orbital Debris Particle: Including the Effects of Temperature and COPV Contents	31
3.1	Introduction.....	31
3.2	Testing Program Overview	32
3.3	Rupture Limit Equation Development.....	33
3.4	Comparison of the Rupture Limit Equation against R/NR Test Data.....	35
3.5	Conclusions.....	36
3.6	Acknowledgments.....	37
3.7	References.....	37
4.0	Rupture Limit Equation for Shielded COPVs	39
4.1	Introduction.....	39

4.2	Rupture Limit Equation Development.....	41
4.3	Comparison of the Rupture Limit Equation against R/NR Test Data.....	43
4.4	Adjustments and Updates to the Equations.....	44
4.5	Conclusions.....	46
4.6	Acknowledgments.....	46
4.7	References.....	47
5.0	Non-Catastrophic Perforation of a Composite Overwrapped Pressure Vessel.....	48
5.1	Introduction.....	48
5.2	Test Parameters Overview	50
5.2.1	Comments on the Testing.....	51
5.3	Ballistic Limit Equation Development	51
5.3.1	COPV BLE Function.....	54
5.3.2	Comparison with Experimental Results	55
5.3.3	Comments on the Data	55
5.4	Hole-Out Area Empirical Modelling	56
5.4.1	Data Overview.....	56
5.4.2	Equation Development	57
5.4.3	Comparison with Experimental Results	58
5.5	Concluding Thoughts.....	60
5.6	Acknowledgements.....	61
5.7	References.....	61

1.0 Rupture of a Cryogenic Composite Overwrapped Pressure Vessel Following a High-Speed Particle Impact

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Abstract

A primary design consideration for such a spacecraft is the anticipation and mitigation of the possible damage that might occur in the event of an on-orbit micro-meteoroid or orbital debris (MMOD) particle impact. While considerable effort has been expended in the study of non-pressurized spacecraft components under room temperature conditions to MMOD impacts, technical and safety challenges have limited the number of tests that have been conducted on pressurized elements of such spacecraft, especially under cryogenic conditions. This paper presents the development of a data-driven equation for composite material pressure vessels under cryogenic operating conditions that differentiates between impact conditions that, given a tank wall perforation, would result in only a small hole or crack from those that would cause catastrophic tank failure. This equation would be useful to a spacecraft designer who might be able to tailor the design parameters and operating conditions of, for example, a fuel tank so that if such a tank were to be struck and perforated by the impact of an MMOD particle, then only a hole would occur and neither catastrophic spacecraft failure nor additional sizable debris would be created as a result of that impact.

1.1 Introduction

Most spacecraft have at least one pressurized vessel on board; for robotic spacecraft, it is usually a liquid propellant tank. In some satellite or spacecraft designs, the fuel tank or some other pressurized vessel is necessarily exposed to the hazards of space, including the micro-meteoroid and orbital debris (MMOD) environment. Because of the potential of serious mission-threatening damage that might result following an on-orbit MMOD impact, one of the primary design considerations of such spacecraft is the anticipation and mitigation of the possible damage that might occur in the event of such an impact.

Considerable energy and effort has been expended in the study of the response of non-pressurized spacecraft components under room temperature conditions to MMOD-like impacts. However, fuel tanks are pressurized internally, and so their main walls will develop bi-axial stress fields because of that internal pressurization. Technical and safety challenges have limited the number of high-speed tests that have been conducted on the pressurized elements of such spacecraft, especially under cryogenic conditions.

In addition to a hole, it is possible that, for certain tank designs, impact parameters, and operating conditions, that a pressurized tank may experience catastrophic failure (i.e. a rupture) as a result of a hypervelocity impact. While a puncture and the resulting leak could result in the destabilization of the spacecraft's orbit, a tank rupture following an on-orbit MMOD impact could lead to loss of the spacecraft and quite possibly, for human missions, loss of life. In order for risk assessments performed during the design phase of a spacecraft to consider and weigh the likelihoods and consequences of the various failures that might occur following an on-orbit MMOD impact, equations are required that can discriminate between combinations of impact parameters and operating conditions that may or may not lead to a catastrophic failure event.

This paper presents the results of an effort directed at addressing one aspect of this problem, namely, the development of a general, data-driven equation for highly pressurized elements such as fuel tanks that differentiates between combinations of impact parameters and operating conditions that would result in only a small hole or crack from those that would cause catastrophic tank failure following a perforating high-speed impact event. This equation is an improvement over a previous version [1] in that the current version is comprised of unitless or non-dimensional terms, whereas the previous version was not. As a result, the equation developed herein is more amendable to incorporating additional, new impact test data and results for similar test conditions as they become available.

The equation to be developed is referred to as a Rupture Limit Equation, or RLE, as it is constructed to distinguish between conditions resulting in either tank rupture or non-rupture. This is an important consideration in the design of a pressurized tank – if possible, design parameters and operating conditions should be chosen such that, in the event of an on-orbit MMOD particle impact, both spacecraft failure and the creation of additional sizable debris pieces (such as those which would be created in the event of tank rupture or catastrophic failure) are avoided.

The RLE presented herein is developed by applying multi-linear regression techniques to data obtained in early studies involving cryogenic COPVs under high-speed impact loading conditions. This, then, is another improvement of the RLE presented herein over the previous version, which was, in effect, merely a hand-drawn or faired curve between two sets of data points. Since the equation in this paper is statistics-based, it (and the statistics associated with it) can easily be included in a risk assessment analysis, whereas the previous version could not.

1.2 Data Overview

There have been many high-speed impact test studies performed using tanks or pressure vessels over the past 50 years. These tests have been performed with varying amounts of internal pressure (including none); with internal fluids, air, or at a vacuum; using metallic tanks and composite material tanks; with spherical tanks as well as cylindrical tanks; using internal fluids at temperatures ranging from room temperature to cryogenic temperatures; and, with and without MMOD shielding. Reference [2] provides a breakdown of the high-speed impact testing that was performed using pressurized tanks over the past 50 years.

In this particular study, we focus on the high-speed impact tests performed on unshielded composite material overwrapped pressure vessels (COPVs) with internal cryogenic fluids. Data from these tests are used to a Rupture Limit Equation (or RLE) equation for these kinds of pressure vessels under these operating conditions distinguish between impact parameters and

operating conditions that would result in either only a small hole or crack (i.e. a non-rupture) from those that would cause catastrophic failure (i.e. a rupture).

The RLE is constructed using data from 24 impact tests - fifteen (15) were performed using so-called “pressure cylinders” (see Figure 1), while nine (9) of the tests were performed using “pressurized bottles” (see Figure 2). The pressure cylinders consisted of one flexible endcap plate made of the material of interest attached to a thick metal cylinder; impacts occurred along a line normal to the flexible endcap test specimen plate. The pressurized bottles consisted of a cylindrical test section to which heavy aluminum end closures were attached to form a closed tank; impacts occurred along a radial line normal to the cylindrical test section.

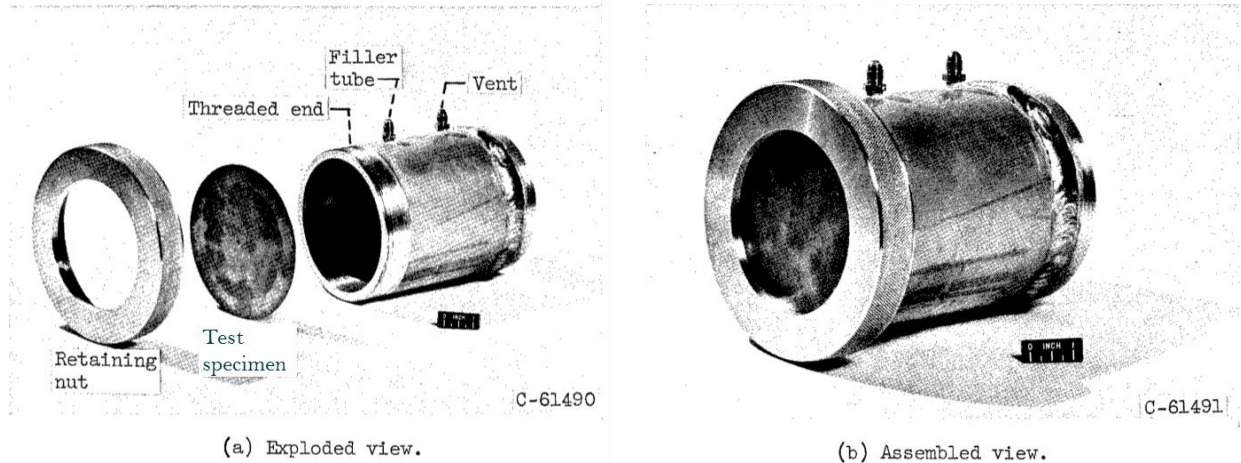


Figure 1. Pressure Cylinder used in High-Speed Impact Testing [3,4]

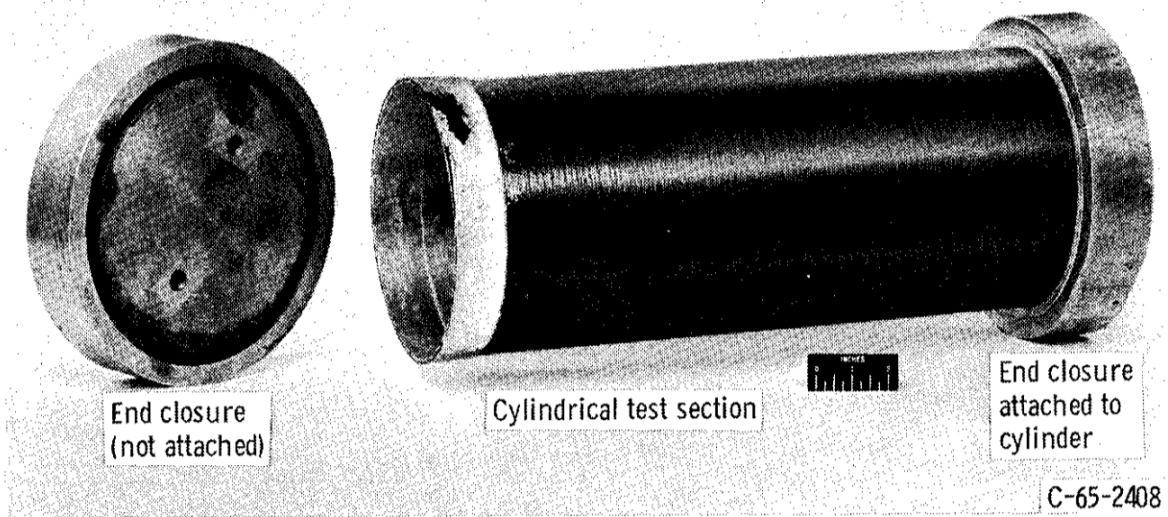


Figure 2. Pressurized Bottle used in High-Speed Impact Testing [4]

Tables 1 and 2a,b present a summary of the impact test conditions and the geometric parameters/material properties of the COPVs used in the development of the RLE, respectively.

Table 1. Overview of Impact Test Conditions

Parameter		Reference [3]	Reference [4]	Units
Projectile	Material	Steel	Steel	
	Density ^a	7.8	7.8	gm/cm ³
	Diameter	5.56	5.56	Mm
Trajectory	Obliquity	0	0	Deg
	Velocity	1.91	1.68	km/s
COPV Contents		LOX	LN2	
COPV Temperature		-300	-330	°F
COPV Pressure		0	~0.34 to ~1.2	MPa

^aNominal average density values from www.matweb.com

The tests performed as reported in References [3] and [4] were motivated by a desire to understand the response of pressure vessels made of composite materials in use at the time those studies were performed to high speed projectile impact. Cryogenic strength values for the composite overwrap and liner materials used in these studies were not typically reported by the investigators for these studies, and so had to be estimated from corresponding room temperature strength values. In some cases, the required room temperature strength values could be found in the literature for composite materials similar in construction to those used in these studies. However, in others, room temperature strength values could not be found and had to be estimated using simple mixture theory and strength values for the component materials involved. Cryogenic strength values were then calculated using published information regarding changes in the strength values of the component materials as the ambient temperature was decreased to cryogenic conditions.

Table 2a. Geometric Parameters and Material Properties of COPVs – Reference [3]

Parameter								Units
COPV Type		Pressure Cylinder						
Composite Plate	Fiber	Glass cloth		Nylon cloth		Dacron		
	Matrix	Epoxy		Phenolic		Polyurethane		
	Tensile Strength	315 ^a	622 ^c	75 ^a	119 ^c	68 ^a	590 ^c	MPa
	Density	2.07 ^d		1.22 ^e		1.30 ^e		g/cm ³
	Thickness	3.175						mm

^aNominal room temp composite strength values estimated from published material strength values

^bNominal room temp composite strength values provided in the cited reference

^cCalculated cryogenic composite or liner strength values

^dNominal composite density values from manufacturer's literature

^eNominal composite density values estimated from published component material density values

^fNominal room temperature liner strength values from www.matweb.com

1.3 Rupture Limit Equation Development\

The empirically-based rupture limit equation (RLE) was developed using the rupture/non-rupture data from References [3] and [4]. To render the equation as broadly applicable as possible, the operating conditions (x-axis) were parameterized as the hoop stress in the tank (non-dimensionalized by the temperature-adjusted uni-axial ultimate tensile stress of the composite overwrap material), and the impact conditions (y-axis) were parameterized as impact momentum

(non-dimensionalized by a number of appropriate tank wall material properties). This approach was used successfully to model the rupture/non-rupture response of cylindrical and spherical metallic tanks [2], as well as air-filled COPVs at room temperature [5] and flat composite plates [6] under high-speed impact loading conditions.

Table 2b. Geometric Parameters and Material Properties of COPVs – Reference [4]

Parameter										Units
COPV Type		Pressure Cylinder						Pressurized Bottle		
COPV Diameter		N/A						19.05		cm
Composite	Fiber	Steel wire		Glass		Dacron		Glass		
	Matrix	Urethane		Urethane		Urethane		Epoxy		
	Tensile Strength	137 ^a	1180 ^c	205 ^a	1771 ^c	68 ^a	590 ^c	772 ^b – 1407 ^b	1016 ^c – 1851 ^c	MPa
	Density	5.65 ^e		2.03 ^e		1.30 ^e		2.07 ^d		g/cm ³
	Thickness	0.508 – 1.60						0.3 – 1.0		mm
Liner	Material	None						Aluminum		
	Tensile Strength	N/A						455 ^f	523 ^c	MPa
	Shear Strength	N/A						273 ^f	314 ^c	MPa
	Yield Stress	N/A						285 ^f	328 ^c	MPa
	Density	N/A						2.80 ^d		g/cm ³
	Thickness	N/A						0.127		mm

^aNominal room temp composite strength values estimated from published material room temp strength values

^bNominal room temp composite strength values provided in the cited reference

^cCalculated cryogenic composite or liner strength values

^dNominal composite density values from manufacturer's literature

^eNominal composite density values estimated from published component material density values

^fNominal room temperature liner strength values from www.matweb.com.

Following on the successful application of this approach in these previous studies, a simple power law form was chosen for the cryogenic COPV RLE to be developed herein. Specifically, the power law for the curve that is intended to separate regions of rupture and non-rupture was chosen as follows:

$$\text{Non-dimensional Projectile Momentum} = A \left(\frac{\sigma_{hoop}}{\sigma_{ult}^{comp}} \right)^B \quad (1)$$

where σ_{hoop} and σ_{ult}^{comp} are the COPV hoop stress and uni-directional ultimate stress of the COPV composite material, respectively. In the case of the pressure cylinder tests, the COPV hoop stresses were taken as the stress values in the composite plate endcaps as given in References [3] and [4]. However, in the case of the pressurized bottle tests, the COPV hoop stresses were calculated using the equation

$$\sigma_{hoop} = \frac{p_{int} r_{OD}}{t_{tot}} \quad (2)$$

where $t_{tot} = t_{comp} + t_{liner}$ is the total nominal thickness of the COPV and is given by the sum of the thicknesses of the composite overwrap and liner (if any), p_{int} is the internal pressure in the COPV, and r_{OD} is the COPV outer radius.

In addition, the non-dimensional form of projectile momentum was taken to be as follows:

$$\text{Non-dimensional Projectile Momentum} = \frac{m_{proj}V_{proj}}{(\rho_{comp}t_{comp}^3)\sqrt{\frac{\sigma_{ult}^{comp}}{\rho_{comp}}}} \quad (3)$$

The first term in the denominator in Eqn (3) has units of mass while the second has units of velocity, thereby rendering the right-hand-side of Eqn (3) unitless, or non-dimensional, so long as there is consistency in the units of mass and velocity used in its numerator and denominator.

To include the effects of other material parameters and the differences between the shapes of the cryogenic test articles, Eqn (3) was modified through the addition of a number of other unitless terms, with the following result:

Non-dimensional Projectile Momentum =

$$\frac{m_{proj}V_{proj}}{(\rho_{comp}t_{comp}^3)\sqrt{\frac{\sigma_{ult}^{comp}}{\rho_{comp}}}} P \left(\frac{\rho_{proj}}{\rho_{comp}} \right)^Q \left(\frac{460+T}{530} \right)^R \left(1 + \frac{\sigma_{ult}^{liner}}{100} \right)^S \left(1 + \frac{\sigma_{yld}^{liner}}{100} \right)^T \quad (4)$$

where the ultimate and yield stresses of the liner material, σ_{ult}^{liner} and σ_{yld}^{liner} , respectively, if there is a liner present, are in MPa. Combining Eqns (1) and (4) yields the final form of the RLE as follows:

$$\frac{m_{proj}V_{proj}}{(\rho_{comp}t_{comp}^3)\sqrt{\frac{\sigma_{ult}^{comp}}{\rho_{comp}}}} P \left(\frac{\rho_{proj}}{\rho_{comp}} \right)^Q \left(\frac{460+T}{530} \right)^R \left(1 + \frac{\sigma_{ult}^{liner}}{100} \right)^S \left(1 + \frac{\sigma_{yld}^{liner}}{100} \right)^T = A \left(\frac{\sigma_{hoop}^{comp}}{\sigma_{ult}^{comp}} \right)^B \quad (5)$$

The coefficient P and the exponents Q , R , S , and T in Eqn (4) were selected so as to allow, as much as possible, a natural separation between the rupture and non-rupture data points. This would, in turn, facilitate the development of a RLE that would, again, as much as possible, lie between those two regions. The attractiveness and benefit of this approach is that if additional tests results were to become available, the values of P , Q , R , S , and T in Eqn (3) could again be adjusted to allow the incorporation of the new rupture/non-rupture data and the subsequent development of a new RLE. To this end, Table 3 presents the values of the coefficient P and exponents Q, R, S , and T used in the non-dimensionalization scheme defined in Eqn (4):

Table 3. Values of P , Q , R , S , and T in Non-dimensionalization Scheme

Exponent	Value	
P	Pressurized Bottle	Pressure Cylinder
	1.0	0.0167
Q	7.0	
R	1.0	
S	-1.0	
T	-1.0	

The constants A and B in Eqn (1) are determined through a linear regression of the rupture/non-rupture data using the Levenberg–Marquardt algorithm [7]. This is a common algorithm for minimizing a function over the space of parameters of the function (i.e. in this particular case, the coefficients in a user-defined function). In addition to solving for coefficients, this algorithm also provides statistical information that can be used to assess the “goodness of fit” of the regression results as well as standard deviation curves for the regression model.

The actual regression exercise was performed by first creating a function Z that would take on values of +1 or -1 depending on whether a particular test resulted in either a rupture or a non-rupture. This function is derived from the linearized form of Eqn (5) and written as follows:

$$Z = W_1 \ln(X) + W_2 \ln(Y) + W_3 \quad (6)$$

where Y is non-dimensionalized projectile momentum and X is non-dimensionalized hoop stress, respectively. It is actually, then, the constants W_1 , W_2 , and W_3 that are obtained through a regression of the test parameters, given by X and Y, against test results given by Z and represented by a +1 or a -1. Once W_1 , W_2 , and W_3 that are obtained, setting $Z = 0$ yields the desired RLE. Furthermore, when we set $Z = 0$, solving for Y gives us the following expression for A and B in terms of the regression parameters W_1 , W_2 , and W_3 :

$$A = \exp\left(-\frac{W_3}{W_2}\right) \quad (7a)$$

$$B = -\frac{W_1}{W_2} \quad (7b)$$

Tables 4 and 5 contain the results of this exercise, namely,

- the coefficients W_1 , W_2 , and W_3 ,
- the correlation coefficient R^2 ,
- the standard deviations of W_1 , W_2 , and W_3 , and
- the covariance matrix for W_1 , W_2 , and W_3 .

Table 4. Results of Regression Exercise

Regression Parameter	Regression Value	Standard Deviation
W_1	-3.2629	1.0158
W_2	-1.8108	0.6076
W_3	3.8888	1.7344
R^2	71.7%	

Table 5. Statistical Information from Regression Exercise

Var(W_1)		(sym)
1.0318		
Covar(W_2, W_1)	Var(W_2)	
0.5487	0.3692	
Covar(W_3, W_1)	Covar(W_3, W_2)	Var(W_3)
-1.0740	-0.9472	3.0083

From the information in Table 4 and Eqns (7a,b), we find that $A = 8.5636$, $B = -1.8019$. This completes the development of the RLE for the test data considered in this study. In the next section we compare the predictions of the RLE against the test data, and offer comments the utility, and limitations, of this RLE.

1.4 Comparison Against Empirical Results

Figure 3 shows plots of the RLE developed herein compared to experimental rupture/non-rupture results. This figure also show plots of $\pm$ one standard deviation curves and $\pm$ two standard deviation curves about the RLE curve. The orange data points represents those tests which resulted in tank rupture, while the green data points shows those which did not for both types of test specimen configurations.

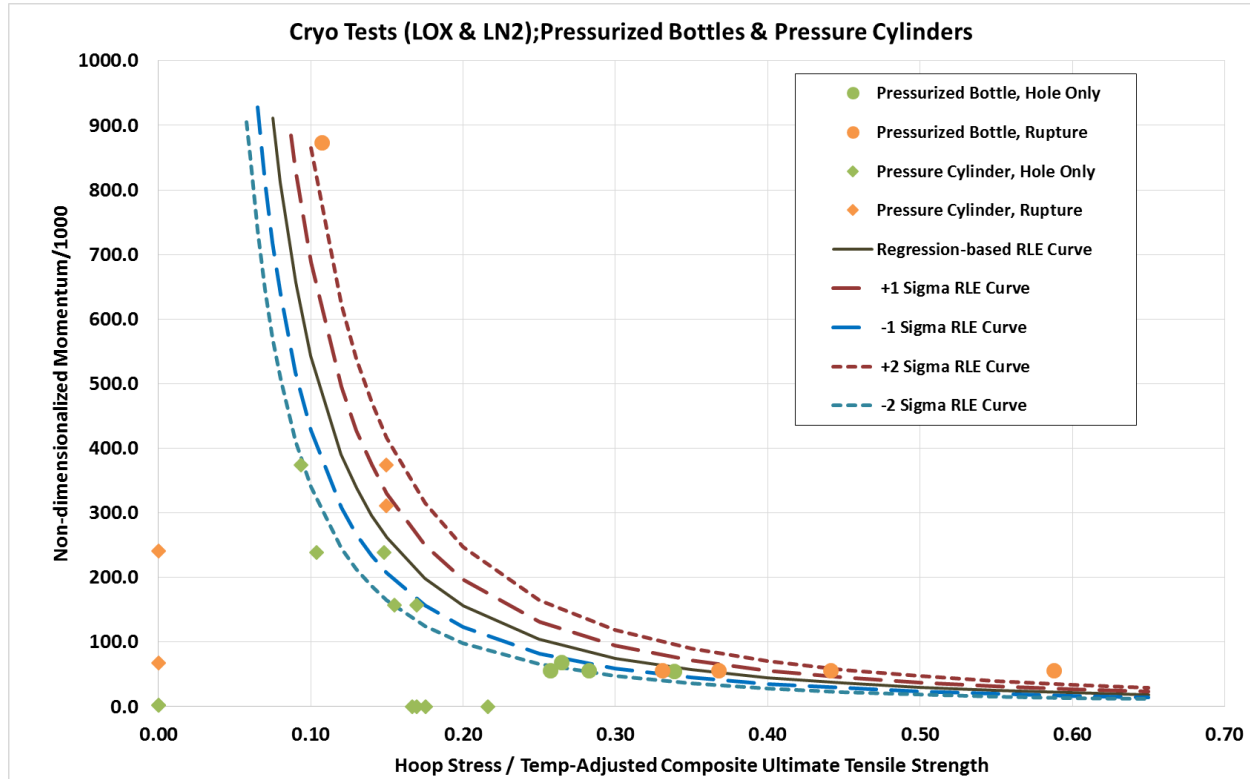


Figure 3. Plots of RLE Curve and Experimental Rupture/No-rupture Data

As can be seen in Figure 3, with the exception of the two ruptured unpressurized pressure cylinder tests, the RLE developed herein appears to predict the rupture/non-rupture response of COPVs at cryogenic temperatures fairly well. This is supported by the “tightness” of the confidence bound curves about the RLE – another indication of the goodness of fit of the RLE to the rupture/non-rupture data. A possible explanation for the rupture of the two unpressurized cylinders was offered in Reference [3] – namely, that the resins in these two tests had high percentages of organic polymeric matter. In the presence of liquid oxygen, this possibly made them highly susceptible to catastrophic response, even without any internal pressure.

Another means for assessing the ability of the RLE to discriminate between the region of impact parameters and operating conditions that would result in rupture from those that would not is through the use of **specificity** and **sensitivity ratios**. In the medical world, these values are frequently used to distinguish between false positives and false negatives. For example, if we

designate a rupture event as the event we are testing for, then an actual rupture might be considered as a “positive reading”, while a non-rupture might be considered as a “negative reading”. As such, the following definitions could be applied for the demarcation line derived from the RLE developed herein:

$$\text{Sensitivity ratio} = (\text{Actual ruptures predicted as ruptures}) / (\text{Actual ruptures predicted as ruptures} + \text{Actual ruptures predicted as non-ruptures}) \quad (8)$$

$$\text{Specificity ratio} = (\text{Actual non-ruptures predicted as non-ruptures}) / (\text{Actual non-ruptures predicted as non-ruptures} + \text{Actual non-ruptures predicted as ruptures}) \quad (9)$$

These ratios yield a quantitative assessment of whether or not the RLE developed herein could be considered as conservative or non-conservative in the following manner:

- a low specificity value (i.e. many non-ruptures predicted as ruptures) and a high sensitivity value (i.e. fewer ruptures predicted as non-ruptures) → a conservative RLE
- a high specificity value (i.e. fewer non-ruptures predicted as ruptures) and a low sensitivity value (i.e. many ruptures predicted as non-ruptures) → a non-conservative RLE

Furthermore, if both values were found to be high, the RLE could be considered as fairly accurate, whereas if both values were low, that would indicate either a problem with the testing method or with test repeatability.

For the data considered herein and the RLE developed based on that data, we find that, even including the two ruptures that lie below the RLE curve, we have a sensitivity value of 0.78 and a specificity value of 0.93 (if the two “odd” rupture data points were removed, the sensitivity value would be exactly 1.0). Based on these results, we can again conclude that the RLE performs rather well in separating the test results where a rupture occurred from those that resulted in non-rupture.

1.5 Concluding Thoughts

This paper presented a summary of the work performed to address a key issue related to the design of pressurized vessels and tanks that are part of a spacecraft that would be built to operate in the MMOD environment. An empirical equation was developed that would differentiate between impact parameters and operating conditions that would result in only a small hole or crack in a COPV with an internal cryogenic fluid from those that would cause catastrophic tank failure. This equation was found to be fairly adept in predicting the rupture/non-rupture response of pressurized COPVs at cryogenic temperatures.

1.6 Acknowledgements

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1.7 References

1. Schonberg, W.P., “Predicting the Rupture of a Cryogenic Composite Pressure Vessel (COPV) Following a Hypervelocity Impact”, Paper No. 166, Proceedings of the 7th European Conference on Space Debris, eds T. Flohrer and F. Schmitz, Darmstadt, Germany, April 2017.
2. Schonberg, W.P., and Ratliff, M., “Hypervelocity Impact of a Pressurized Vessel: Comparison of Ballistic Limit Equation Predictions with Test Data and Rupture Limit Equation Development”, *Acta Astronautica*, Vol. 115, 2015, pp. 400-406.
3. Dengler, R.P., *An Experimental Investigation of Chemical Reaction Between Propellant Tank Material and Rocket Fuels or Oxidizers When Impacted by Small High - Velocity Projectiles*, NASA TN-D-1882, NASA Lewis Research Center, Cleveland, OH, August 1963.
4. Stepka, F.S., *Projectile-Impact-Induced Fracture of Liquid-Filled, Filament-Reinforced Plastic or Aluminum Tanks*, NASA TN-D-3456, NASA Lewis Research Center, Cleveland, OH, June 1966.
5. Schonberg, W.P., “Rupture of Composite Pressure Vessels (COPVs) Following a Hypervelocity MMOD Particle Impact”, Paper No. AIAA-2018-0231, Proceedings of the AIAA SciTech Forum, Kissimmee, FL, January 2018.
6. Schonberg, W.P. “Predicting the Catastrophic Failure of Composite Plates Following a Hypervelocity Impact”, Proceedings of the 30th International Symposium on Ballistics, Long Beach, California, September 2017.
7. PSIPlot: Scientific Spreadsheet and Technical Plotting User’s Guide, Poly Software International, Pearl River, NY, 2010.

2.0 Will it Leak? Will it Burst? COPV Perforation and Rupture after a MMOD Impact

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Abstract

Most spacecraft have at least one pressurized vessel on board. Because of the severe damage that might result following a high-speed on-orbit space debris particle impact, a primary design consideration is the anticipation and mitigation of that damage. Depending on pressure vessel design and impact/operating conditions, a pressure vessel impacted at hypervelocity may experience either only relatively shallow damage; a through-hole, perhaps with localized liner cracking or composite peeling; or catastrophic failure (rupture). While a puncture and the resulting leak could de-stabilize an orbiting spacecraft, an on-orbit rupture could not only lead to spacecraft loss, but for human missions, possibly loss of life. Herein we present the development of a ballistic limit equation (BLE) and a rupture limit equation (RLE) for composite overwrapped pressure vessels. Similar to a BLE that can be used to characterize whether or not a pressure vessel would be punctured by a high-speed impact, an RLE is designed to differentiate between regions of operating and impact conditions that, given a perforation, would result in either a rupture or would result only in a relatively small hole or crack. In a risk assessment that considers the various failures that might occur following debris impact, both types of equations are required. Comparisons of the RLE and the BLE developed herein with experimental results shows that both equations are able to cleanly separate the regions of rupture from non-rupture, and perforation from non-perforation. As such, the equations presented are both highly accurate in predicting the response of the COPVs and impact conditions considered.

2.1 Introduction

Most spacecraft have at least one pressurized vessel on board. For robotic spacecraft, it can be a liquid propellant tank, while for human spacecraft, there are the pressurized living quarters (or modules). Because of the potential of serious mission-threatening damage that might result following an on-orbit micro-meteoroid or orbital debris (MMOD) impact, one of the primary design considerations of such spacecraft is the anticipation and mitigation of the possible damage that might occur in the event of such an impact. To prevent mission failure and possibly loss of

life, protection against failure caused by high-speed impacts must be included in spacecraft design.

Considerable energy and effort have been expended in the study of the response of flat unstressed (i.e., non-pressurized) spacecraft components under hypervelocity impact conditions intended to simulate those of on-orbit MMOD impacts. However, fuel tanks are pressurized internally, and so their main walls will develop bi-axial stress fields because of that internal pressurization. Until recently, technical and safety challenges have limited the number of tests that have been conducted on the pressurized elements of such spacecraft. Especially limited has been the number of hypervelocity impact tests performed on composite overwrapped pressure vessels (COPVs). In an effort to address this issue, a testing and analysis program has been undertaken to characterize the hypervelocity impact response of COPVs.

In addition to a hole, it is possible that, for certain COPV designs, materials, impact parameters, and operating conditions, a COPV may experience catastrophic failure (i.e., rupture) as a result of a hypervelocity impact. This was demonstrated in a spectacular fashion when an oxygen-filled Kevlar-overwrapped aluminum pressure vessel ruptured in an explosive fashion following the 6.4 km/s impact of a 3.18 mm aluminum sphere [1].

By way of illustration, Figure 1 below shows an impacted COPV that sustained only a relatively small perforation, while Figure 2 shows a ruptured COPV. Whether or not a structural element, such as a panel, wall, or even a battery or circuit board is perforated by or is considered to be in a failed state as a result of a hypervelocity impact is typically characterized by a ballistic limit equation (BLE). These equations are traditionally derived from data-based curves which are drawn to distinguish between regions of, for example, perforation and non-perforation (P/NP) in terms of impact parameters and element configuration materials, geometries, etc. While the puncture of a pressurized vessel and the resulting leak could result in the de-stabilization of a spacecraft orbit, a pressurized tank rupture following an on-orbit MMOD impact could lead to loss of spacecraft and quite possibly, for human missions, loss of life.

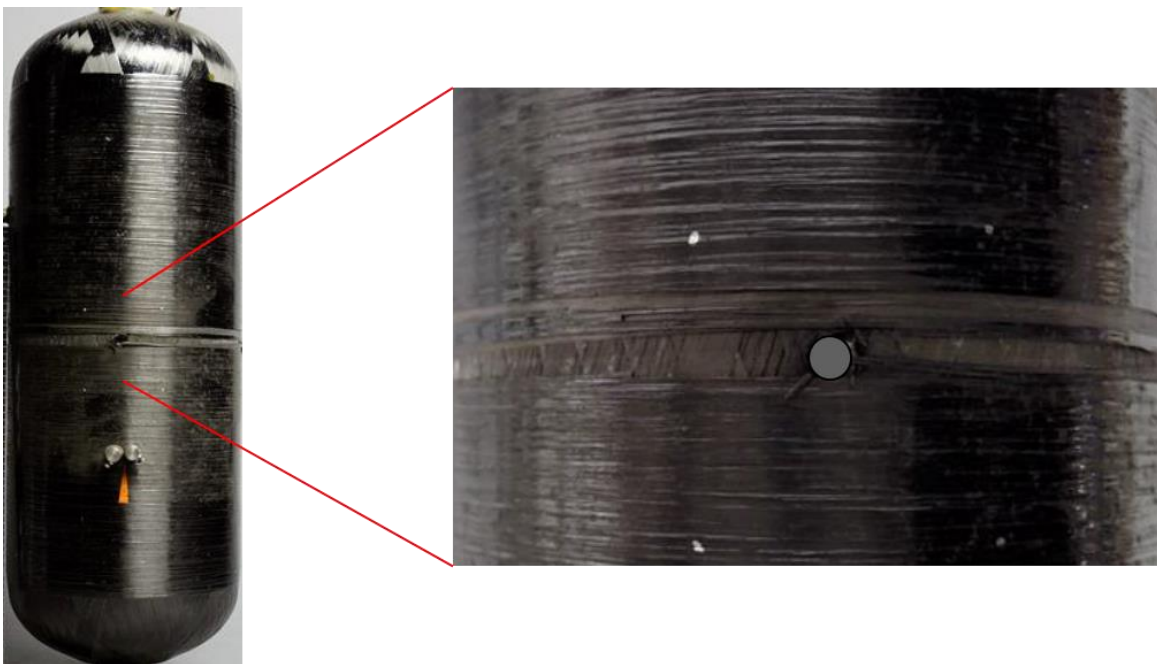


Figure 1. Example of a Perforated COPV

In a manner similar to that of a ballistic limit equation, a rupture limit equation (RLE) can be used to characterize whether or not rupture would occur following a perforating impact. Such an equation can be developed in a fashion again similar to that used in BLE development, namely, by forming a curve that distinguishes between regions of rupture and non-rupture (R/NR) in terms of impact parameters, element configurations, geometries, etc. In a risk assessment that considers the various failures that might occur following debris impact, both types of equations are required.



Figure 2. Example of a Ruptured COPV

In this paper, we present the development of these two types of equations for COPVs impacted by hypervelocity particles. The RLE is designed to differentiate between regions of operating and impact conditions that, given a perforation, would result in either a rupture or only a relatively small hole or crack. Similarly, the BLE is constructed so that it distinguishes between impact and operating conditions that would result in a front side puncture (without rupture) from those that would not.

2.2 Testing Program Overview

Data from approx. 50 high speed impact tests on several different types of COPVs are pooled together and used in the development of the RLE presented herein. Tables 1 and 2 provide a summary of the impact test conditions and the geometric parameters and material properties of the COPVs used in the various test programs that generated the data that were subsequently used in the development of the RLE and the BLE. Additional information regarding these tests can be found in References [2-6].

Table 1. Overview of Impact Test Conditions

Parameter		Ref. [2,3]						Ref. [4]		Refs. [5], [6]	Units
		Phase A		Phase B		Phase C					
Projectile	Material	Al 2017-T4	440C SS	Al 2017-T4	440C SS	Al 2017-T4	440C SS	Al 2017-T4	440C SS	Aluminum	
	Density ^a	2.79	7.63	2.79	7.63	2.79	7.63	2.79 ^a	7.63 ^a	2.8 ^b	gm/cm ³
	Diameter	1.0	1.09,	2.71,	1.70	1.00	0.090	0.6 –	0.5,	8.0	mm
		– 2.5	1.29	3.40, 5.01	– 1.99	– 1.30	– 0.129	3.1	1.0		
Trajectory	Obliquity	0, 45, 60	45	0, 45	45	45	45	0, 45	0, 45	0	deg
	Velocity	~4.09, ~7.05	~7.06	~7.00	~7.00	~7.0 0	~7.00	~7.03	~6.99	~4.35 – ~4.46	km/s
Impact Location		Cylinder		Cylinder		Cylinder		Cylinder		Cylinder	
Contents		Air		Air		Air		Air		Water, GN2	
Pressure		0, ~29		~31		~21		0		0.1, 20 – 50	

^aNominal values from www.matweb.com, ^bAssumed average nominal values

2.2.1 Comments on the Data

It is important to note that only tests in which the projectile impacted the cylindrical portion of the COPV were considered in the development of the RLE and the BLE. While a number of the tests in Refs. [2] and [3] were conducted with impacts occurring on the shoulder or hoop-to-dome-transition region (the so-called HTDTR region), these tests were not included because of the complexity of the stress fields in this region. While hoop stresses in the cylindrical portion of a COPV could be reasonably approximated using fundamental mechanics-based equations, the dependence of the HTDTR hoop stress on dome shape as well as internal pressure, thickness, etc., precluded the development of a simple HTDTR hoop stress equation from being developed and used in subsequent analysis and RLE development.

Table 2. Geometric Parameters and Material Properties of COPVs

Parameter	Ref. [2,3]				Ref. [4]	Refs. [5], [6]	Units	
	Phase A	Phase B	Phase C					
COPV Diameter (O.D)	16.75	42.5	12.7	21.1	42.5	10	cm	
COPV Length (Cylinder)	34.3	54.6	29.2	25.4	88.6	31	cm	
Composite	Fiber	T1000	T1000	T1000	T1000	CFRP		
	Matrix	LRF-092	HARF-53	SIREZ	SIREZ	HARF-53	CFRP	
	Tensile Strength ^a	3040	3040	3040	3040	3040 ^a	1515 ^e	MPa
	Density ^a	1.56	1.56	1.56	1.56	1.63 ^a	1.65 ^e	gm/cm ³
	Thickness ^b	2.54	6.35	3.64	3.64	6.95	4.8	mm
Liner	Material	Al 6061-T6	Inconel	Al 6061-T6	Al 6061-T6	Inconel	Aluminum	
	Tensile Strength ^c	455	1282	455	455	1282 ^c	455 ^e	MPa
	Shear Strength ^c	273	882	273	273	882 ^c	273 ^e	MPa
	Yield Stress ^c	285	1041	285	285	1041 ^c	285 ^e	MPa
	Density ^c	2.70	8.19	2.70	2.70	8.19 ^c	2.8 ^e	gm/cm ³
	Thickness ^d	1.98	0.81	1.52	1.70	0.75	2.3	mm

^aNominal value from manufacturer's literature^bNominal composite thickness in cylindrical portion of COPV^cNominal values from www.matweb.com^dNominal liner thickness in cylindrical portion of COPV^eAssumed average nominal values

2.3 Rupture Limit Equation Development

The RLE was developed using the R/NR data from Refs. [2-6]. To render the equation as broadly applicable as possible, the operating conditions (x-axis) were parameterized as the hoop stress in the tank (non-dimensionalized by the uni-directional ultimate stress of the tank wall composite material), and the impact conditions (y-axis) were parameterized as impact momentum (non-dimensionalized by a number of appropriate tank wall material properties). This approach was used successfully to model the R/NR response of cylindrical and spherical metallic tanks [7], COPVs under cryogenic operating conditions [8], and flat composite material plates under high-speed projectile impact [9].

Following on the successful application of this approach in the previous studies noted above, a simple power law form was chosen for the RLE to be developed. Specifically, the power law for the curve that separates regions of rupture and non-rupture was chosen as follows:

$$\text{Non-dimensional Projectile Momentum} = A \left(\frac{\sigma_{hoop}}{\sigma_{ult}} \right)^B \quad (1)$$

where σ_{hoop} and σ_{ult}^{comp} are the COPV hoop stress and uni-directional ultimate stress of the COPV composite material, respectively. COPV hoop stresses were calculated using the equation

$$\sigma_{hoop} = \frac{p_{int} r_{OD}}{t_{tot}} \quad (2)$$

where $t_{tot} = t_{comp} + t_{liner}$ is the total nominal thickness of the cylindrical portion of the composite material overwrap, p_{int} is the internal pressure in the COPV, and r_{OD} is the COPV outer radius.

The non-dimensional form of projectile momentum was taken initially to be given as follows:

$$\text{Non-dimensional Projectile Momentum} = \frac{m_{proj} V_{proj}^{norm}}{(\rho_{comp} t_{tot}^3) \sqrt{\frac{\sigma_{ult}^{comp}}{\rho_{comp}}}} \quad (3)$$

where V_{proj}^{norm} is the normal component of the impact velocity. The first term in the denominator in Eqn (3) has units of mass while the second has units of velocity, thereby rendering the right-hand-side of Eqn (3) unitless, or non-dimensional, so long as there is consistency in the units of mass and velocity used in its numerator and denominator.

To include the effects of other material parameters, Eqn (3) was modified through the addition of a number of other unitless terms, with the following result:

$$\text{Non-dimensional Projectile Momentum} = \frac{m_{proj} V_{proj}^{norm}}{(\rho_{comp} t_{tot}^3) \sqrt{\frac{\sigma_{ult}^{comp}}{\rho_{comp}}}} \left(\frac{\rho_{proj}}{\rho_{comp}} \right)^Q \left(1 + \frac{\sigma_{ult}^{liner}}{100} \right)^S \left(1 + \frac{\sigma_{yld}^{liner}}{100} \right)^T \quad (4)$$

where the ultimate and yield stresses of the liner material, σ_{ult}^{liner} and σ_{yld}^{liner} , respectively, are in MPa. The liner material yield and ultimate stress values were divided by 100 so the terms involving these quantities could have a meaningful effect without being excessively large compared to the values of the other terms in the equation. Combining Eqns (1) and (4) yields the final form of the RLE as follows:

$$\frac{m_{proj} V_{proj}^{norm}}{(\rho_{comp} t_{tot}^3) \sqrt{\frac{\sigma_{ult}^{comp}}{\rho_{comp}}}} \left(\frac{\rho_{proj}}{\rho_{comp}} \right)^Q \left(1 + \frac{\sigma_{ult}^{liner}}{100} \right)^S \left(1 + \frac{\sigma_{yld}^{liner}}{100} \right)^T = A \left(\frac{\sigma_{hoop}}{\sigma_{ult}^{comp}} \right)^B \quad (5)$$

The particular forms of the non-dimensionalized hoop stress and impact momentum seen in Eqns (1)–(5) were motivated by the desire to include those terms that not only characterized the impact loads and internal pressurization conditions, but at the same time included material parameters that were seen to differ among the various test programs considered herein. As such, the terms used in Equations (1)–(5) are consistent with the identifying test specimen and material parameters shown in Table 1. Furthermore, the ability to use these equations does not rely on having to know the exact lay-up or stacking sequence of the composite laminae in the COPV – all that is needed is the uni-axial ultimate tensile strength of the composite material. This serves to increase the ease of use of these equations and their potential applicability to a wider family of COPV constructions.

The exponents Q, S, T in Eqn (4) were selected so as to allow, as much as possible, a natural separation between the ruptured and non-ruptured data points. This would, in turn, facilitate the

development of a RLE that would, again, as much as possible, lie between those two regions. The attractiveness and benefit of this approach is that if additional tests results were to become available, the exponents Q, S, T in Eqn (3) could again be adjusted to allow the incorporation of whatever new R/NR becomes available and the subsequent development of a new RLE. To this end, the values of the exponents Q, S, T used in the non-dimensionalization scheme defined in Eqn (4) are $Q = 0.1$ and $S = T = -1.0$.

The constants A and B in Eqn (1) were determined using two different approaches. The first was a regression-based analysis of the R/NR data, and was performed by creating a function that would take on a different numerical value depending on whether a certain combination of non-dimensional hoop stress and projectile momentum resulted in a rupture or a non-rupture event. The second was a discriminant line approach that sought to fit a single line through all the R/NR data when pooled together, thereby separating regions of rupture from non-rupture. These approaches and the results obtained using each are described in detail in the following sections.

2.3.1 Regression Analysis Approach

The constants A and B in Eqn (1) are first determined through a linear regression of the R/NR data using the Levenberg–Marquardt algorithm [10]. This is a common algorithm for minimizing a function over the space of parameters of the function (i.e., the coefficients in a user-defined function). In addition to solving for the unknown coefficients, the algorithm also provides statistical information that can be used to assess the “goodness of fit” of the regression results and to develop standard deviation curves for the regression model.

This initial regression exercise was actually performed by first creating a function Z that would take on values of +1 or -1 depending on whether a particular test resulted in either a rupture or a non-rupture. This function is derived from the linearized form of Eqn (5) and written as follows:

$$Z(X, Y) = W_1 \ln(X) + W_2 \ln(Y) + W_3 \quad (6)$$

where Y is non-dimensionalized projectile momentum and X is non-dimensionalized hoop stress, respectively. It is actually, then, the constants W_1, W_2 , and W_3 that are obtained through a regression of the test parameters, given by X and Y , against test results given by Z and represented by a +1 or a -1. Once W_1, W_2 , and W_3 are obtained, setting $Z = 0$ and solving for Y in terms of X yields the desired RLE, which has the form

$$Y = AX^B \quad (7)$$

where A and B are given in terms of the regression parameters W_1, W_2 , and W_3 as follows:

$$A = \exp\left(-\frac{W_3}{W_2}\right) \quad (8a)$$

$$B = -\frac{W_1}{W_2} \quad (8b)$$

Tables 3 and 4 contain the results of this regression exercise, namely, the coefficients W_1, W_2 , and W_3 , the correlation coefficient R , the standard deviations of W_1, W_2 , and W_3 , and the covariance matrix for W_1, W_2 , and W_3 .

Table 3. Results of Regression Exercise

Regression Parameter	Regression Value	Standard Deviation
W_1	-0.3359	0.1334
W_2	-0.3562	0.0903
W_3	-2.1003	0.5975
R	0.743	

From the information in Table 3 and Eqns (8a,b), we find that $A = 2.166 \times 10^{-3}$, and $B = -0.943$. This completes the development of the RLE for the test data considered in this study. In the next section we compare the predictions of this RLE against the test data, and offer comments on its utility and limitations.

Table 4. Statistical Information from Regression Exercise

VAR (W ₁)			(sym)
0.01780			
COV(W ₂ ,W ₁)	VAR(W ₂)		
0.00537	0.00814		
COV(W ₃ ,W ₁)	COV(W ₃ ,W ₂)	VAR(W ₃)	
0.05982	0.04634	0.35701	

2.3.2 Comparison of Regression-based RLE against Empirical R/NR Results

Figures 3 and 4 show plots of the RLE developed using the process outlined in the previous section and compared to experimental rupture/non-rupture results. These figures also show plots of $\pm$ one standard deviation curves and $\pm$ two standard deviation curves about the RLE curve. Figure 3 is a plot of all the data, while Figure 4 is a “zoomed-in” version of Figure 3 to allow closer inspection of the cluster of data at lower momentum values. In Figures 3 and 4, the orange data points represent those tests that resulted in tank rupture, while the green data points show those that did not rupture, but did sustain a perforation through the composite overwrap and internal metallic liner. The blue data points correspond to those tests in which perforation of the COPV liner did not occur, although in such tests the composite overwrap did sustain damage, even down to the metallic liner in some cases.

The $\pm$ one and $\pm$ two standard deviation curves about the RLE curve shown in Figures 3 and 4 were obtained using the following procedure. First, we calculate the standard deviation, $STD(Z)$, of the linearized prediction equation given by Eqn (6) as follows [11]:

$$\begin{aligned}
 [STD(Z)]^2 = & [STD(W_1) \ln(X)]^2 + [STD(W_2) \ln(Y)]^2 + [STD(W_3)]^2 + \\
 & 2[COV(W_1, W_2) \ln(X) \ln(Y)] + 2[COV(W_1, W_3) \ln(X)] + 2[COV(W_2, W_3) \ln(Y)]
 \end{aligned} \quad (9)$$

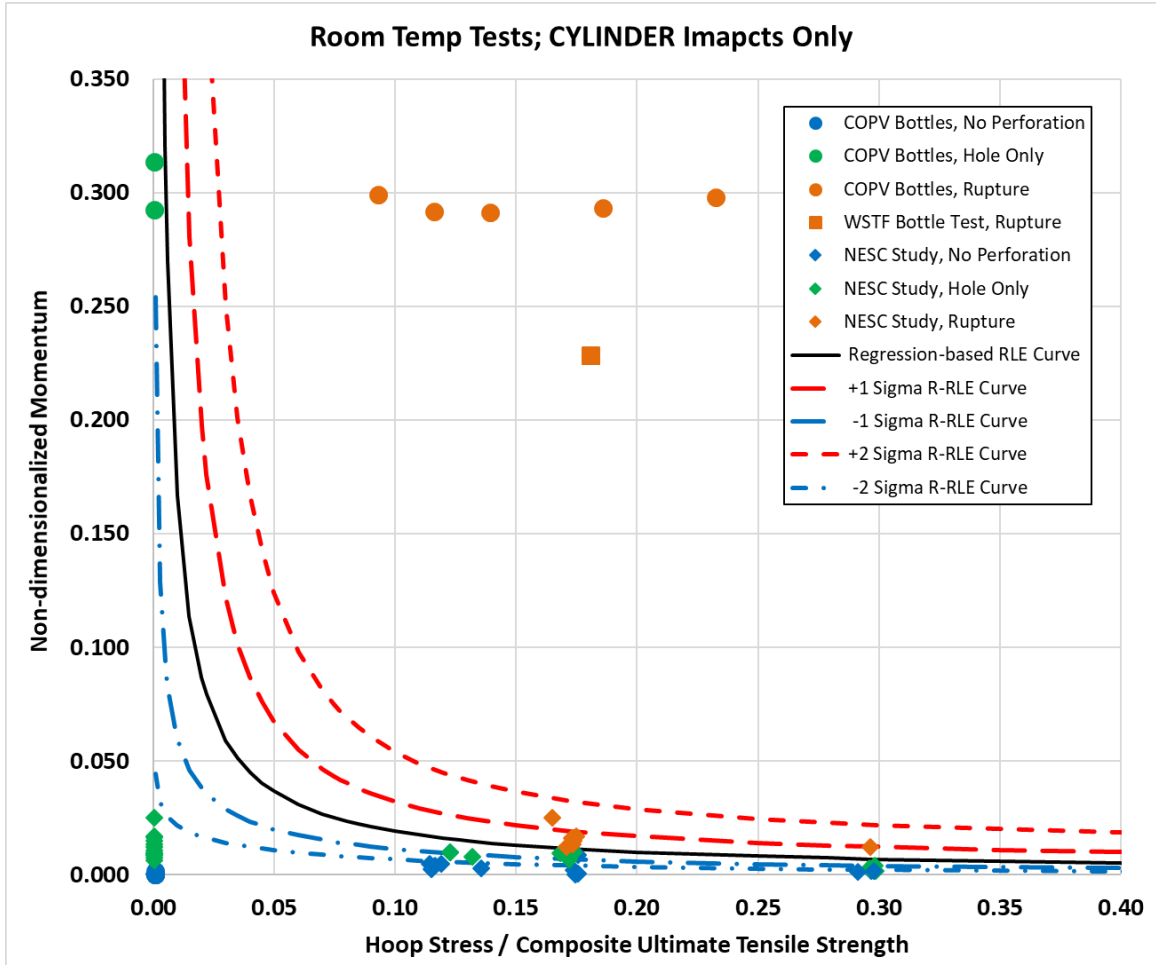


Figure 3. Plots of Regression-based RLE Curve, Standard Deviation Curves, and Experimental Rupture/Non-rupture Data

In Eqn (9), the co-variances, or $COV()$, of the parameters W_1 , W_2 , and W_3 are given in Table 4. Likewise, the standard deviations, or $STD()$, of these parameters are given in Table 3 (and are also, of course, the square roots of the corresponding variances, or $VAR()$, in Table 4). It is important to note that in Eqn (9) the points (X,Y) actually lie on the RLE, that is, on the curve found previously by setting $Z = 0$ in Eqn(6), and then solving for Y in terms of X .

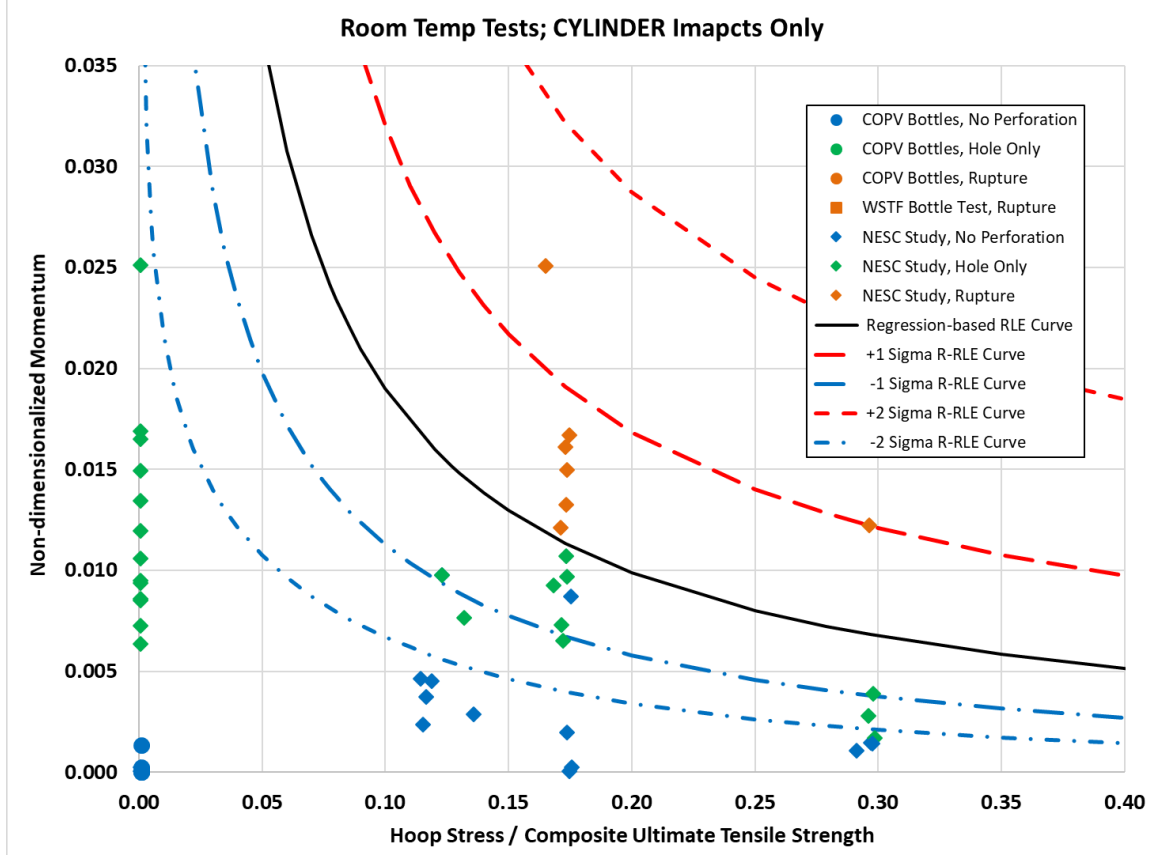


Figure 4. Zoomed in Plots of Regression-based RLE Curve, Standard Deviation Curves, and Experimental Rupture/Non-rupture Data

Next, for example, to find the + one standard deviation curve, Z' , we write

$$Z' = Z + STD(Z) \quad (10)$$

Substituting for Z according to Eqn (6), we have

$$Z' = W_1 \ln(X) + W_2 \ln(Y') + W_3 + STD(Z) \quad (11)$$

where now we have used Y' instead of Y to indicate that when we let $Z' = 0$ in Eqn (11) we will obtain the functional form of the + one standard deviation curve in X - Y coordinate space by solving for Y' .

So then, setting $Z' = 0$ in Eqn (11) and solving for Y' in terms of X yields the desired + one standard deviation curve, which has the form

$$Y' = A' X^B \quad (12)$$

The quantities A' and B are given in terms of the standard deviation $STD(Z)$ of the RLE at a given X value and the corresponding Y value on the RLE as calculated by Eqn (7), and the regression parameters W_1 , W_2 , and W_3 as follows:

$$A' = \exp \left(-\frac{W_3 + STD(Z)}{W_2} \right) \quad (13a)$$

$$B = -\frac{w_1}{w_2} \quad (13b)$$

Thus, the construction of, for example, the + one standard deviation curve about the RLE curve is accomplished using the following step-by-step process:

1. Choose an X value
2. Calculate the corresponding Y value for the RLE from Eqn (7)
3. Calculate STD(Z) at the point on the RLE given by (X,Y) using Eqn (9)
4. Calculate the corresponding Y' value for the + one standard deviation curve corresponding to X using Eqns (12) and (13a,b)
5. Choose next X value. Repeat.

In a similar fashion, the – one standard deviation and ± 2 standard deviation curves can be found by replacing STD(Z) in Eqn (13a) with $-\text{STD}(Z)$, and $\pm 2\text{STD}(Z)$, respectively.

A closer look the regression-based RLE and the $\pm$ one and $\pm$ two standard deviation curves about the RLE curve in Figures 3 and 4 reveals the following points of interest.

1. The RLE shown in Figure 3 as well as in Figure 4 appears to make a clean cut between the green (non-rupture) and orange (rupture) data points. Thus, the RLE curve developed using this approach is 100% accurate in discriminating between COPV ruptures and non-ruptures as indicated by the data obtained thus far.
2. The RLE curve appears to be “skewed” to lie closer to the NR (green) data points near an ordinate value of ~ 0.30 . However, this is merely an artifact of the regression process which was performed using log values of actual non-dimensionalized hoop stress and projectile momentum.
3. The confidence bounds on the RLE shown in Figures 3 and 4 appear to be a bit wide. However, this is not unexpected for the following reasons.
 - a. As can be seen in these figures, the data are grouped in four clusters: horizontally near a non-dimensional momentum value of approx. 0.3 (i.e. the test data from Refs. [5,6]), and vertically about non-dimensional hoop stress values of ~ 0 , ~ 0.127 , ~ 0.173 , and ~ 0.297 (i.e. the test data in Refs. [2-4]). Although the data from unpressurized tests (i.e., those with hoop stress values of ~ 0) were not used in the development of the RLE using this approach, wide confidence bounds such as those shown in Figures 3 and 4 are to be expected considering the distribution of the test data that were used in the development of this RLE.
 - b. The validity of any regression analysis is predicated on the assumption of relatively continuous changes in dependent variable values with respect to corresponding changes in the values of the independent variables. In this exercise, values of +1 or -1 were assigned to the dependent variable function, depending on whether a particular test resulted in either a rupture or a not-rupture. This approach, while certainly capable of producing a curve fit for this type of non-continuous dependent variable data, is well-known to produce highly conservative error bounds, which are clearly evident in Figures 3 and 4.
4. While this RLE is satisfactory in terms of its ability to discriminate between regions of rupture and non-rupture, the wide confidence bounds in Figures 3 and 4 indicate that the RLE could be anywhere in the confidence region, and that it contains sensitive parameters whose slight variation could possibly significantly change the location and

shape of the curve. Until and unless additional tests are performed to generate data points in other regions of the plots shown in these figures, it is unlikely that best fit curves with “tighter” confidence bounds can be developed using this approach. If a reduction in the uncertainty bounds shown in Figures 3 and 4 is desired, a more sophisticated or robust approach for determining alternative curves that discriminate between rupture and non-rupture data points is required.

5. The decided lack of overlapping of the rupture/non-rupture test data is primarily due to the judicious selection of the values of the exponents Q , S , and T . However, this lack of overlap is admittedly rather unusual, especially in hypervelocity impact test programs (see, e.g., the overlap and scatter in such testing of metallic dual-wall systems [12]). It is indeed possible that additional testing of similar COPVs could yield test results that cannot be so cleanly separated. In this case, there might be quite a bit of overlap and engineering judgement will be needed regarding where to best place the RLE (as well as the associate BLE) so as to best meet program needs and requirements. For example, in a program where a more conservative curve is required (e.g., one in which crew safety is paramount), the RLE can be shifted “downward” amongst any overlapping data. However, if a higher element of risk is acceptable in a particular program, then it can be shifted “upward” amongst the overlapping R/NR data.

2.3.3 Discriminant Line Analysis Approach

In this approach, the constants A and B in Eqn (1) are determined using linear discriminant analysis (see, e.g., [13]). This method attempts to classify observations described by continuous variables into two or more groups (e.g., rupture or non-rupture when described by combinations of hoop stress and impact momentum). This is accomplished by developing a fitting model that estimates the distance from each observation to each group’s multivariate mean, or centroid, and then classifying each observation into their closest groups. When there are only two classification groups, this process is equivalent to a linear regression of all the data pooled together. This regression exercise was performed using the following linear model:

$$\ln(Y) = \alpha + \beta \ln(X) \quad (14)$$

where again X and Y refer to non-dimensionalized hoop stress and impact momentum, respectively. Once α and β are obtained, we can easily form the expression for the discriminant-based RLE with the following result:

$$Y = AX^B \quad (15)$$

where α and β are given as follows:

$$A = \exp(\alpha), B = \beta \quad (16a,b)$$

Tables 5 and 6 contain the results of this regression, namely, the coefficients α and β , the correlation coefficient R , standard deviations of α and β , and the covariance matrix for α and β .

Table 5. Results of Regression Exercise

Regression Parameter	Regression Value	Standard Deviation
α	-5.6893	0.3238
β	-0.6595	0.1214
R	0.446	

Table 6. Statistical Information from Regression Exercise

VAR (α)	(sym)
0.09336	
COV(β , α)	VAR(β)
0.02925	0.01426

From the information in Table 6 and Eqns (16a,b), we find that $A = 3.555 \times 10^{-3}$, $B = -0.6595$. This completes the development of the discriminant-based RLE for the test data considered in this study. In the next section we compare the predictions of this RLE against the test data, and against the regression-based RLE developed in the previous section.

2.3.4 Comparison of Discriminant-based RLE against Empirical R/NR Results

Figures 5 and 6 show plots of the discriminant-based RLE developed using the process outlined in the preceding section and compared to experimental rupture/no-rupture results as well as the previously developed regression-based RLE. These figures also show plots of $\pm$ one standard deviation curves and $\pm$ two standard deviation curves about the discriminant-based RLE curve. Figure 5 is again a plot of all the data, while Figure 6 is a “zoomed-in” version of Figure 5 to allow closer inspection of the cluster of data at lower momentum values.

As before, in Figures 5 and 6 the orange data points represent those tests that resulted in tank rupture, while the green data points show those that did not rupture, but did sustain a perforation through the composite overwrap and internal metallic liner; the blue data points correspond to tests in which perforation of the COPV liner did not occur, although in such tests the composite overwrap did sustain damage, even down to the metallic liner in some cases.

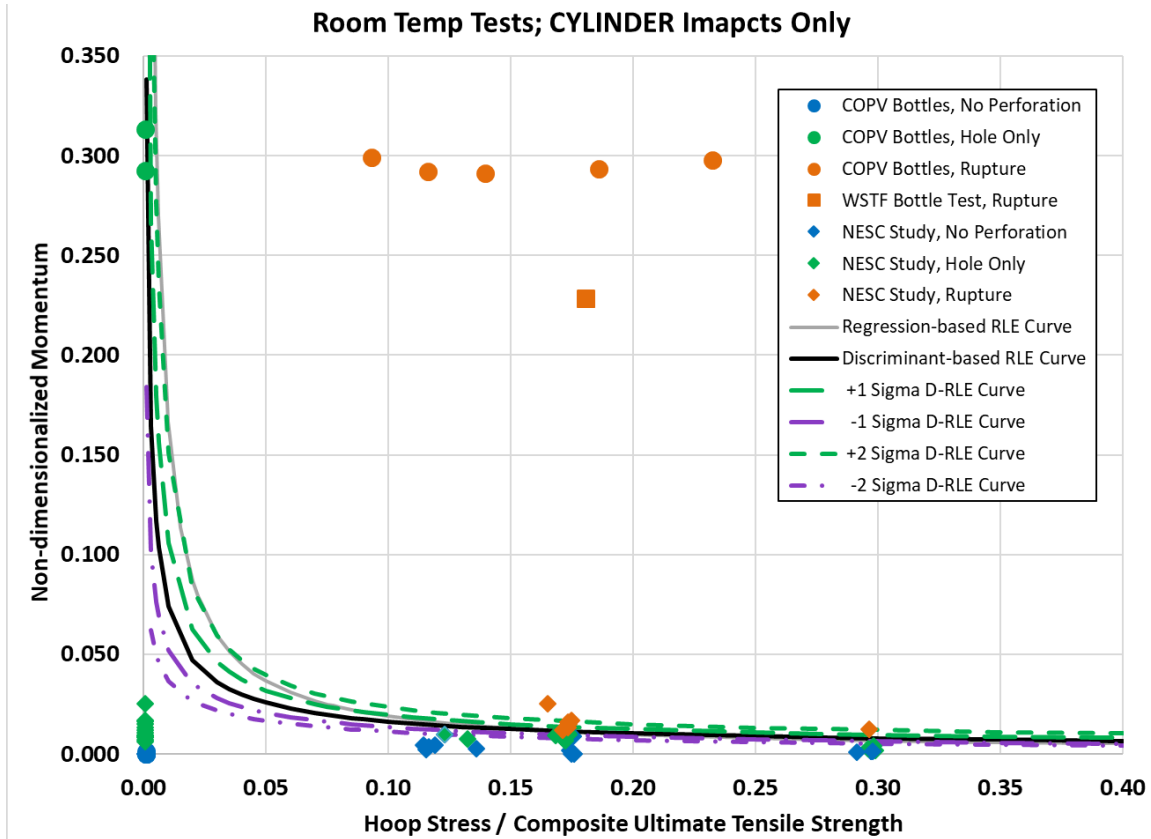


Figure 5. Plots of Discriminant-based RLE Curve, Standard Deviation Curves, Experimental Rupture/Non-rupture Data, and Regression-based RLE Curve

The $\pm$ one and $\pm$ two standard deviation curves about the discriminant-based RLE curve shown in Figures 5 and 6 were obtained using the procedure outlined previously. A closer look at both of the RLEs and the $\pm$ one and $\pm$ two standard deviation curves about the discriminant-based RLE curve drawn in Figures 5 and 6 reveals several points of interest.

1. The discriminant-based RLE appears to be less conservative than the regression-based RLE for higher internal pressures, but more conservative for lower impact pressures. That is, for higher internal pressures, the discriminant-based RLE is placed higher than, and so would predict more non-ruptures than, the regression-based RLE. For lower impact pressures, the opposite appears to be the case – it is placed lower than the regression-based RLE and so predicts more ruptures than the regression-based RLE. However, considering the large areas of non-dimensional momentum/non-dimensional hoop stress combinations that are devoid of data, it is at this time not possible to speculate as to whether the regression-based or discriminant based RLE might be “more accurate” or “more correct.”

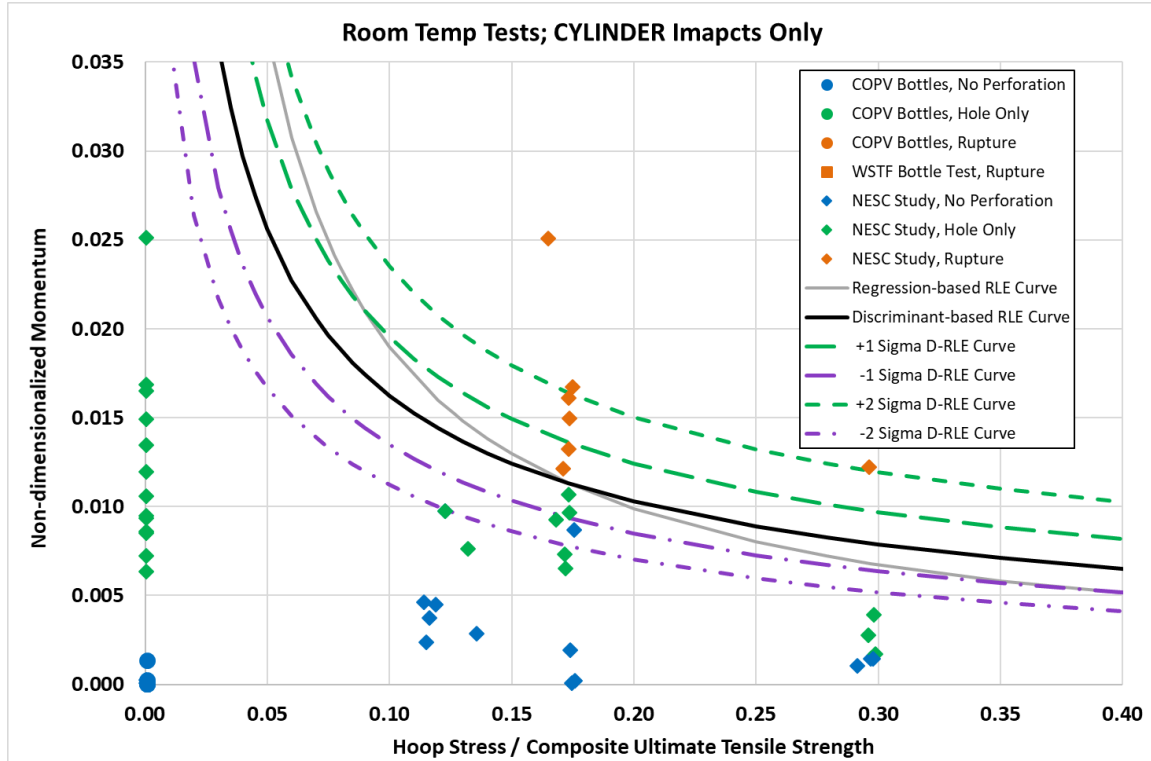


Figure 6. Zoomed in Plots of Discriminant-based RLE Curve, Standard Deviation Curves, Experimental Rupture/Non-rupture Data, and Regression-based RLE Curve

2. Second, the uncertainty bands for the discriminant-based RLE in Figures 5 and 6 appear to a bit “tighter” than those for the regression-based RLE as seen previously in Figures 3 and 4. Since the data used in both cases was the same, this is likely an artifact of the modelling technique. As discussed previously, the regression-based approach of assigning +1 or -1 to a dependent variable function, while capable of producing a curve fit even for this type of non-continuous dependent variable information, is also known to produce the wide error bands that are evident in Figures 3 and 4. However, in the discriminant-based approach, since there were only two classification groups and these two groups are fairly distinct, developing the model that fitted “in between” these two groups through a regression of the pooled set of all the data should have, and did, result in fairly tight confidence bounds.

2.4 Ballistic Limit Equation Development

As projectile diameter was increased while impact velocity was kept (relatively) constant, it was found that before projectiles were large enough to cause rupture of a COPV, there were impact conditions that resulted in merely front side perforation of the COPV (even under highly pressurized conditions). While catastrophic failure following a high-speed MMOD particle impact would be disastrous to a mission, a perforation and the ensuing thrust caused by the expulsion of fluids or gas from the perforated tank could result in the de-stabilization of the spacecraft’s orbit (see, e.g., [14]). This could then lead to spacecraft loss and for human missions, loss of life. As a result, an equation is needed that would distinguish between combinations of impact parameters and operating conditions that would result in a puncture (without rupture) of the front side of an impacted pressure vessel from those that would not. This

equation was developed previously in Reference [15]; a summary of its development is presented herein for completeness. Additional information and details about the BLE development can be found in Reference [15].

2.4.1 Comments on the Data

Except for a single non-perforation (blue) data point in Figure 4, all of the perforation (but non-rupture, or green) data points appear to be grouped separately from the other non-perforation tests. This should allow for the fairly straightforward development of a simple ballistic limit equation, or BLE, for the COPVs considered in this study, based on the test data obtained thus far. With regard to the apparent “errant” non-perforation data point noted above, a review of the results for this test and that for another test of the same COPV type and under similar operating conditions revealed that it was on the verge of being penetrated [15].

2.4.2 Equation Development

Considering, then, the rather clean separation of the penetration (green) and non-penetration (blue) data points in Figure 4, we now construct a ballistic limit equation (BLE) for the COPVs considered in this study to predict the onset of “front face” liner perforation of these and similar COPVs. This was accomplished by adjusting the coefficients of a relatively simple function so that the final form of the function met the following conditions with regard to the P/NP data points in Figure 4:

1. the resulting BLE curve should lie between the highest non-penetration (blue) and lowest penetration (green) data points when the non-dimensional hoop stress was ~ 0 , ~ 0.125 , ~ 0.175 , and ~ 0.30 ; and,
2. the resulting BLE curve should approach zero as the non-dimensional hoop stress approached unity.

After several iterations, the final form of the BLE was found to be given as follows:

$$\text{Non-dimensional Projectile Momentum} = P_0 \frac{1 - e^{-C(1 - \sigma_{hoop}/\sigma_{ult}^{comp})^E}}{1 - e^{-C}} \quad (17)$$

where values of the constants in Eqn (21) were found to be $P_0 = 6.0 \times 10^{-3}$, $C = 4.0$, and $E = 7.35$.

2.4.3 Comparison of the BLE against Empirical P/NP Results

Figure 7 shows a plot of the proposed BLE amid the pertinent data. As can be seen from Figure 7, the BLE developed in this fashion, with the exception of the single “errant” point noted above, correctly discriminates between regions of liner perforation and non-perforation.

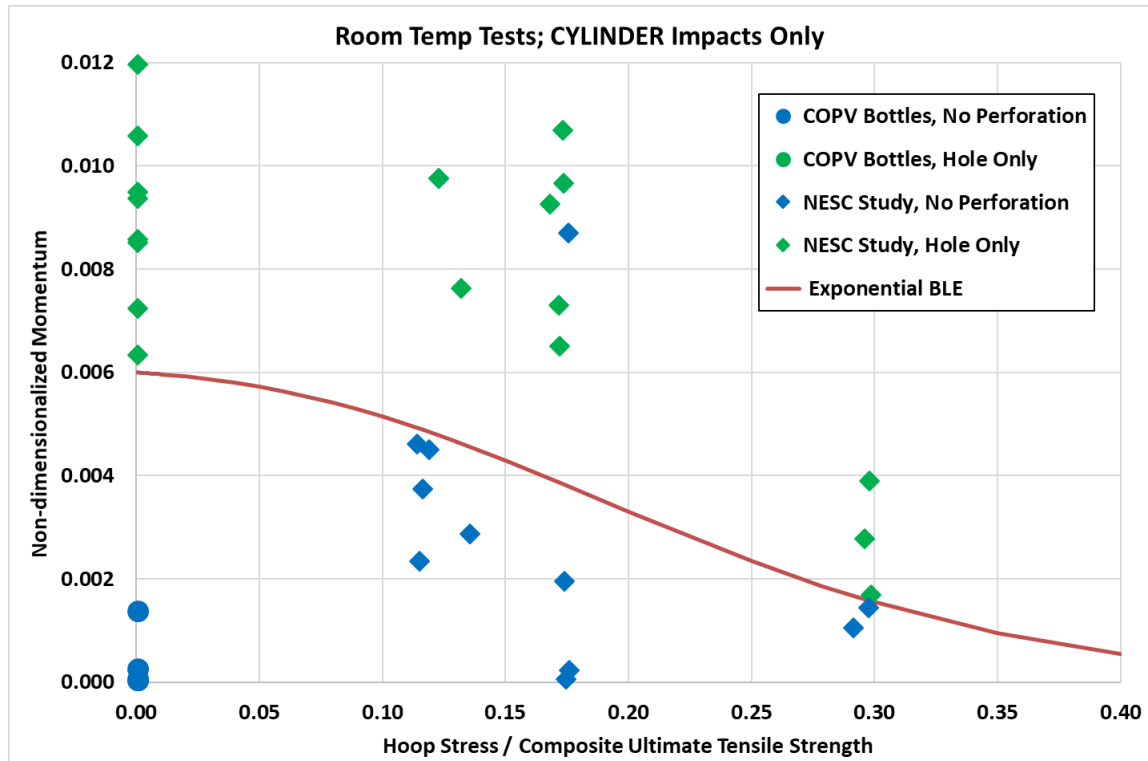


Figure 7. Plot of Exponential BLE Curve and Experimental Perforation/Non-Perforation Data

2.5 Practical Considerations When Applying the COPV RLE and BLE

As can be seen from Figures 3-7 above, the RLEs and BLEs developed using R/NR and P/NP data are a good fit to that data in that they are both able to successfully separate events (i.e., ruptures or penetrations) from non-events (i.e., non-ruptures or non-penetrations). This successfully demonstrates that the methodology presented herein can be used to develop such equations, and that it can be updated as needed when additional R/NR or P/NP data becomes available.

An important practical application of the RLE and BLE developed herein is predicting whether or not a passivated (i.e., empty, or nearly empty) pressure vessel on board an orbiting spacecraft will be ruptured or will be perforated by an MMOD impact during the End-of-Life part of its mission. In addition to the limitations noted previously, the following features of the data and the equations must be noted, understood, and hopefully addressed in the future.

With only a few notable exceptions, the high-speed impact testing performed to date has been done with graphite/epoxy fibers; with inert gases or no pressure; at room (or slightly elevated) temperatures; and, using cylindrical-type COPVs. Questions naturally arise regarding how to apply equations the equations developed as part of this study to, for example, orbital environments (e.g., temperatures); to COPVs pressurized with cryogenic fluids, or with propellants or oxidizers; for other COPV shapes, fibers, etc.; and, when the COPV is “shielded” by other spacecraft components. To address these issues, the following suggestions for future work are made.

1. Some high-speed impact test data involving COPVs pressurized with cryogenic fluids does exist. A previous study used this data to develop an RLE very similar to that

developed in this study (see again, [8]). A natural combination of the two data sets might be possible, and the RLE could be modified, possibly through the addition of a temperature term of some kind, so that the resulting (new) RLE fits both the cryogenic and room temperature R/NR and P/NP data.

2. Of course, the presence of a fluid within the COPV could affect its rupture or ballistic limit thresholds. Ideally, some additional room-temperature high-speed tests with similar fluid-filled COPVs at similar impact conditions would allow a comparison between response characteristics of air-filled and fluid-filled COPVs. The effects of the presence of a fluid in a COPV can, perhaps, be incorporated in the RLE or BLE through, perhaps, an additional density term in the equations.
3. There have been a number of recent impact studies performed using hydrazine-filled COPVs (see, e.g., [16,17]). Experimental results and analytical modelling techniques should be examined for possible inclusion and use in updating the RLE and BLE developed as part of this study to include the effects of propellant on COPV response.
4. There have been a number of analytical, numerical, and experimental studies shielded COPVs impacted by high-speed particles (see, e.g., [18-21]). Data from these studies should be examined for possible inclusion and use in updating the RLE and BLE developed as part of this study to include the effects of shielding on rupture and penetration response.

2.6 Conclusions

A study was performed to generate data to support the development of two new response predictor equation for COPVs. Using this data, a rupture limit equation (RLE) was successfully developed that can be used to discriminate between combinations of projectile momentum and COPV hoop stress that likely will or will not result in an impact-induced rupture of a COPV similar to the ones considered in this study. Similarly, a ballistic limit equation (BLE) was successfully developed that can be used to discriminate between combinations of projectile momentum and COPV hoop stress that likely will or will not result in an impact-induced puncture (without rupture) of a COPV similar to the ones considered in this study. In both cases, the RLE and BLE developed were able to correctly discriminate between regions of COPV rupture and non-rupture, and perforation and non-perforation, respectively. As such, the equations presented herein are highly accurate in predicting the rupture/non-rupture and perforation/non-perforation responses of the COPVs considered in this study. Further testing is needed, particularly for impact momentum and hoop stress combinations not yet considered, and for other COPV configurations and constructions, in order to further refine the RLE and BLE and to render them applicable under a wider range of impact scenarios and operating conditions.

2.7 Acknowledgments

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2.8 References

1. Rucker, M.A., Deeson, H., Stoltzfus, J., and Benz, F., Demonstration of a Hypervelocity Impact Test Capability, NASA WSTF TR-692-001, Johnson Space Center, Houston, TX, September 1991.
2. Davis, B.A., Christiansen, E.L., Miller, J.E., and Yoder, T.B., “Hypervelocity Impact of Composite Overwrap Pressure Vessels”, Paper No. 6102, Proceedings of the 1st International Orbital Debris Conference, Sugarland, Texas, December 2019.
3. Garcia, M.A, Davis, B.A., and Miller, J.E, “Hypervelocity Impact Testing of a Pressurized Composite Overwrapped Pressure Vessel and Comparison to Numerical Analysis”, *Procedia Engineering*, Vol. 204, 2017, pp. 476–483.
4. Nahra, H., Nichols, C., Christiansen, E., Greene, N., Miller, J., Davis, B., Composite Over-wrapped Pressure Vessel (COPV) Hypervelocity Impact (HVI) Study: Phase – 1 (Impacts on Unpressurized COPV Tank), Johnson Space Center, Houston, TX, 2015.
5. Hereil, P.L., Mespoulet, J., and Plassard, F., “Hypervelocity Impact of Aluminum Projectiles against Pressurized Aluminum-Composite Vessel”, *Procedia Engineering*, Vol. 103, 2015, pp. 181-188.
6. Hassin, J., Pfaab, K., Puillet, C., Limido, J., Lacome, J.L., and Hereil, P., “Hypervelocity Impacts on Composite Overwrapped Pressurized Vessels”, Proceedings of DYNCOMP 2015, Arles, France, June 2015.
7. Schonberg, W.P., and Ratliff, M., “Hypervelocity Impact of a Pressurized Vessel: Comparison of Ballistic Limit Equation Predictions with Test Data and Rupture Limit Equation Development”, *Acta Astronautica*, Vol. 115, 2015, pp. 400-406.
8. Schonberg, W.P., “Rupture of a Cryogenic Composite Overwrapped Pressure Vessel Following a High-Speed Particle Impact”, *Aerospace*, Vol. 5, 2018, paper no. 20.
9. Schonberg, W.P. “A Rupture Limit Equation for Pre-Loaded Laminated Composite Plates”, *Journal of Composites Science*, Vol. 2, 2018, paper no. 3.
10. PSIPlot: Scientific Spreadsheet and Technical Plotting User’s Guide, Poly Software International, Pearl River, NY, 2010.
11. Draper, N., and Smith, H., Applied Regression Analysis, John Wiley, New York, 1981.
12. Schonberg, W.P., and Compton, L.E., “Application of the NASA/JSC Whipple Shield Ballistic Limit Equations to Dual-Wall Targets under Hypervelocity Impact”, *International Journal of Impact Engineering*, Vol. 35, 2008, pp. 1792-1798.
13. JMP 13 Multivariate Methods, 2nd Edition, SAS Institute, Inc., Cary, NC, 2017.
14. Celestian, J.P., and Schonberg, W.P., “Dynamic Response of the Space Station Freedom due to a Module Perforation by a Hypervelocity Impact”, *International Journal of Impact Engineering*, Vol. 13, No. 2, 1993, pp. 353-365.
15. Schonberg, W.P., “Non-Catastrophic Perforation of a Composite Overwrapped Pressure Vessel”, *Acta Astronautica*, Vol. 174, 2020, pp. 41-47.
16. Limido, J., Puillet, C., and Vila, J-P, “Explosion of Hydrazine Tanks Due to Space Debris Impacts”, Proceedings of the 2019 Hypervelocity Impact Symposium, Destin, FL, April 2019.
17. Zhao, B., Zhao, J., Cui, C., and Wang, Y., “Shock Initiation of a Satellite Tank under Debris Hypervelocity Impact”, *Applied Sciences*, Vol. 9, 2019.
18. Telitchev, I.Y., and Prokhorov, A.G., “Damage Parameters Analysis of Shielded Pressure Vessels under Space Debris Impact”, Proceedings of the 2nd European Conference on

Space Debris, Darmstadt, Germany, Publication No. ESA-SP-393, pp. 435-443, March 1997.

19. Telitchev, I.Y., Schaefer, F., Schneider, W., and Lambert, M., “Analysis of the Fracture of Gas-Filled Pressure Vessels under Hypervelocity Impact”, *International Journal of Impact Engineering*, Vol. 23, 1999, pp. 905-919.
20. Mullin, S.A., Coque, H., Cour-Palais, B.G., Grosch, D.J., and Walker, J.D. “Bursting of Shielded Pressure Vessels Subject to Hypervelocity Impact”, *International Journal of Impact Engineering*, Vol. 20, 1997, pp. 579-590.
21. Telitchev, I.Y., “Analysis of Burst Conditions of Shielded Pressure Vessels Subjected to Space Debris Impact”, *Journal of Pressure Vessel Technology*, Vol. 27, 2005, pp. 179-183.

3.0 Predicting the Rupture of a COPV Impacted by a High-Speed Orbital Debris Particle: Including the Effects of Temperature and COPV Contents

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Abstract

A main design consideration for spacecraft destined to operate in the meteoroid or orbital debris (MOD) environment is the prevention of possible damage that might occur following an MOD impact. Risk assessments that are performed during spacecraft design typically consider what might happen following an on-orbit MOD impact. In order to perform such an assessment, a variety of response prediction equations are required, including equations that predict whether a structural element, such as a pressure vessel or fuel tank, is perforated or ruptured by an MOD impact. In this paper, we bring together the datasets from room temperature gas-filled and cryogenic liquid-filled pressure vessel testing programs to develop a single equation that predicts whether tank rupture will occur following a high-speed impact. Comparison with test results shows that the equation developed correctly predicts the rupture/non-rupture responses of the pressure vessels considered in this study. This new RLE can now predict whether or not a COPV would rupture under a much wider range of temperatures, constructions, and COPV contents.

3.1 Introduction

If an earth-orbiting spacecraft is impacted by a meteoroid or orbital debris (MOD) particle, there exists the possibility of mission- and life-threatening damage. Because of this, a major aspect of the spacecraft design process is the prevention of possible damage that might occur following such an impact. While the study of flat and unstressed spacecraft components under hypervelocity impact is well-documented, testing of pressurized spacecraft components, which are curved and engender bi-axial stress fields, has been limited (especially using composite overwrapped pressure vessels, or COPVs). There has, in fact, been some limited testing and analysis using flat metal plates where in-plane bi-axial stress fields were induced in the panels using mechanical tension grips. In these tests, the (relatively low) stress level values induced in the plates corresponded to those that would exist in a pressurized space station module (see, for example, Schonberg, 1993). However, the use of curved panels to simulate and include curvature effects on impact response of plates, whether stressed or not, has not yet been attempted.

Information provided by these kinds of testing programs and the predictor models developed from that information are often part of risk assessment calculations that are performed at several points during the design phase of a spacecraft. Since the early 1990s, NASA has used BUMPER to conduct risk assessments for its spacecraft (see, e.g., Lear, et al, 2019). This assessment considers and weighs what might happen to a spacecraft following an on-orbit MOD particle impact. In order to be able to perform such an assessment, a variety of performance prediction equations are required. For example, in addition to a hole, it is possible that a pressurized vessel could fail catastrophically (i.e., rupture), and not just be perforated following a hypervelocity impact by a piece of orbiting space debris. However, at present, equations that predict catastrophic failure or rupture are not readily available and so are not yet used in spacecraft risk assessments.

As used here, the term “rupture” refers to an end state of the COPV following a high-speed impact, ranging from extensive cracking and delamination that would leave the COPV in one or two pieces to complete shatter and disintegration, as illustrated in Schonberg (2020). Whether or not a spacecraft pressure vessel (such as a fuel tank) could rupture following an on-orbit high-speed impact by an orbital debris particle is of special interest because such catastrophic failure events have the potential to produce tremendous amounts of additional orbital debris particles, some of which could be quite large. As such, equations that predict catastrophic failure of a pressure vessel are very much needed by spacecraft risk assessments.

Equations that predict perforation or rupture are usually based on empirical curves that separate regions of perforation from non-perforation (P/NP) or rupture from non-rupture (R/NR). Equations that separate regions of P/NP are called Ballistic Limit Equations (BLEs). Similarly, Rupture Limit Equations, or RLEs, are used to separate regions of R/NR. Schonberg (2016) presents a history of BLE development for common multi-wall spacecraft systems. This discussion includes insights into the techniques used in their development, as well as limitations in terms of their applicability and the kinds of information they provide.

An RLE was recently developed by Schonberg (2020) for gas-filled cylindrical COPVs under hypervelocity impact at room temperature. Prior to that, Schonberg (2018) developed an RLE for cylindrical COPVs under similar impact conditions, but filled with cryogenic fluids. In those efforts, a novel statistics-based process was developed and implemented to determine the best positioning of a curve between two response regimes. Until those efforts, the placement of such “faired curves”, as they have been called, has been mainly a judgement call (see, e.g. Avva, 1982, 1983, and Schaefer, et al, 1997). In this paper, we bring together the datasets from the room-temperature gas-filled and cryogenic liquid-filled testing programs to develop a single RLE that will be applicable across the full range of temperatures and filling fluids considered in the combined dataset. Comparison with test data shows that the new RLE correctly discriminates between regions of COPV rupture and non-rupture for the COPVs considered in this study.

3.2 Testing Program Overview

Data from 56 high-speed impact tests at ambient temperatures were combined and with 9 tests on COPVs filled with fluids at cryogenic temperatures. Tables 1 and 2 provide an overview of impact test conditions and COPV geometries, respectively, in these tests. Additional information on the room temperature tests, i.e., those used in Schonberg (2020), can be found in Davis, et al (2019), Garcia, et al (2017), Nahra, et al (2015), Hereil, et al (2015), and Hassin, et al (2015);

details regarding the cryogenic temperature tests used in Schonberg (2018) can be found in Stepka (1966).

Table 1. Impact Test Conditions Overview

Parameter	Schonberg (2018)	Schonberg (2020)	Units
Projectile Material	Steel	AL 2017-T4, 440 SS	-----
Projectile Diameter	5.56	~1.0 – 8.0	mm
Trajectory Obliquities	0	0, 45, 60	deg
Impact Velocities	~1.7	~4 – ~7	km/s
Tank Contents	LN2	Air, GN2	-----
Tank Temperatures	–330	70 – 130	deg-F
Tank Pressures	~0.3 – ~1.2	0, 20,30, 50	MPa

Table 2. COPV Geometries and Material Properties

Parameter	Schonberg (2018)	Schonberg (2020)	Units
COPV Diameter (O.D.)	19	12 – 42	cm
COPV Length (Cylinder)	46	30 – 90	cm
Composite	Fiber	Glass	T-1000
	Matrix	Epoxy	LRF092, HARF53, SIREZ
	Thickness	0.3 – 1.0	2.54 – 6.95 mm
Liner	Material	Aluminum	AL 6061-T6, Inconel
	Thickness	0.127	0.75 – 2.3 mm

3.3 Rupture Limit Equation Development

Using the procedure originally developed by Schonberg and Ratliff (2015) and later used in Schonberg (2018) and Schonberg (2020), a simple power law was chosen to relate the ordinate (non-dimensional projectile momentum) to the abscissa (non-dimensional hoop stress) in a manner that would delineate between regions of R/NR. The form of the power law for the RLE curve that would serve this purpose was thus written as follows:

$$\text{Non-dimensional Projectile Momentum} = A \left(\frac{\sigma_{hoop}}{\sigma_{ult}} \right)^B \quad (1)$$

where σ_{hoop} and σ_{ult}^{comp} are the COPV hoop stress and the composite material uni-directional temperature-adjusted ultimate stress (or maximum in-plane tensile stress), respectively. The hoop stress was calculated using the standard relationship for cylindrical pressure vessels:

$$\sigma_{hoop} = \frac{p_{int} r_{OD}}{t_{tot}} \quad (2)$$

where $t_{tot} = t_{comp} + t_{liner}$ is the total COPV wall thickness at the center of the cylindrical portion of the COPV, p_{int} is the COPV internal pressure, and r_{OD} is the outer radius of the COPV.

To include the effects of other temperature and COPV contents, the non-dimensional form of projectile momentum used in Schonberg (2020) was adjusted by adding a some other dimensionless terms, with this final result:

Non-dimensional Projectile Momentum =

$$\frac{m_{proj} v_{proj}^{norm}}{(\rho_{comp} t_{tot}^3) \sqrt{\frac{\sigma_{ult}^{comp}}{\rho_{comp}}}} \left(\frac{\rho_{proj}}{\rho_{comp}} \right)^Q \left(\frac{460 \text{ } ^\circ F + \tau}{530 \text{ } ^\circ F} \right)^R \left(1 + \frac{\sigma_{ult}^{liner}}{100 \text{ MPa}} \right)^S \left(1 + \frac{\sigma_{yld}^{liner}}{100 \text{ MPa}} \right)^T \left(\frac{\rho_{air}}{\rho_{cont}} \right)^U \quad (3)$$

where V_{proj}^{norm} is the normal component of impact velocity; σ_{ult}^{liner} and σ_{yld}^{liner} are temperature-adjusted liner material ultimate and yield stresses, respectively, in MPa; τ is the test or COPV contents temperature in $^\circ F$; and, ρ_{air} and ρ_{cont} are the densities of air (at room temperature) and the COPV contents, respectively. In Eqn (3), the momentum in the numerator of the first term has been non-dimensionalized by $\rho_{comp} t_{tot}^3$, which has units of mass, and $\sqrt{\sigma_{ult}^{comp} / \rho_{comp}}$, which has units of velocity; all the other terms in the right-hand-side of Eqn (3) are unitless. It is for these reasons that the right-hand-side of Eqn (3) is referred to as “non-dimensional projectile momentum.”

The various terms in Eqn (3) were chosen to capture the key differences in material properties, geometries, etc. that differentiated one COPV from another. The values of the yield stress and ultimate stress for the liner material were divided by 100 MPa in order that these terms would have a meaningful effect in the subsequent statistical analyses (that is, without being overly larger or smaller than the values of the other terms in the equation). Values of yield stress, density, etc. for the materials listed in Table 3 and used in Eqn (3) were obtained from various online material properties databases.

Combining Eqns (1) and (3) yields the final form of the RLE as follows:

$$\frac{m_{proj} v_{proj}^{norm}}{(\rho_{comp} t_{tot}^3) \sqrt{\frac{\sigma_{ult}^{comp}}{\rho_{comp}}}} \left(\frac{\rho_{proj}}{\rho_{comp}} \right)^Q \left(\frac{460 \text{ } ^\circ F + \tau}{530 \text{ } ^\circ F} \right)^R \left(1 + \frac{\sigma_{ult}^{liner}}{100 \text{ MPa}} \right)^S \left(1 + \frac{\sigma_{yld}^{liner}}{100 \text{ MPa}} \right)^T \left(\frac{\rho_{air}}{\rho_{cont}} \right)^U = A \left(\frac{\sigma_{hoop}}{\sigma_{ult}^{comp}} \right)^B \quad (4)$$

A significant advantage of this particular type of formulation is the ability to use this equation without needing to know the precise stacking sequence of the composite – all that is required is its uni-axial temperature-adjusted ultimate tensile strength. The exponents Q through U in Eqn (4) were manually selected to facilitate a natural separation between the rupture (R) and non-rupture (NR) test results. The values of these exponents were determined to be $Q = 0.1$, $R = 0.3$,

$S = T = -1.0$, and $U = 1.22$. The constants A and B in Eqn (1) were then determined through a regression analysis of the R/NR data. This process is also described in detail in Schonberg (2020). Following this approach, we find that $A = 1.769 \times 10^{-3}$, $B = -1.058$ (correlation coefficient of 70%).

3.4 Comparison of the Rupture Limit Equation against R/NR Test Data

Plots of the RLE developed herein and corresponding empirical R/NR test results are shown in Figures 1 and 2; Figure 1 is a plot of all the data, while Figure 2 is a “zoomed-in” version of Figure 1. In Figures 1 and 2, the solid black data points are those tests where tank rupture occurred, the grey data points are tests that did not result in rupture but did sustain a through perforation of the COPV, and the hollow data points indicate tests where there was not even a perforation of the COPV.

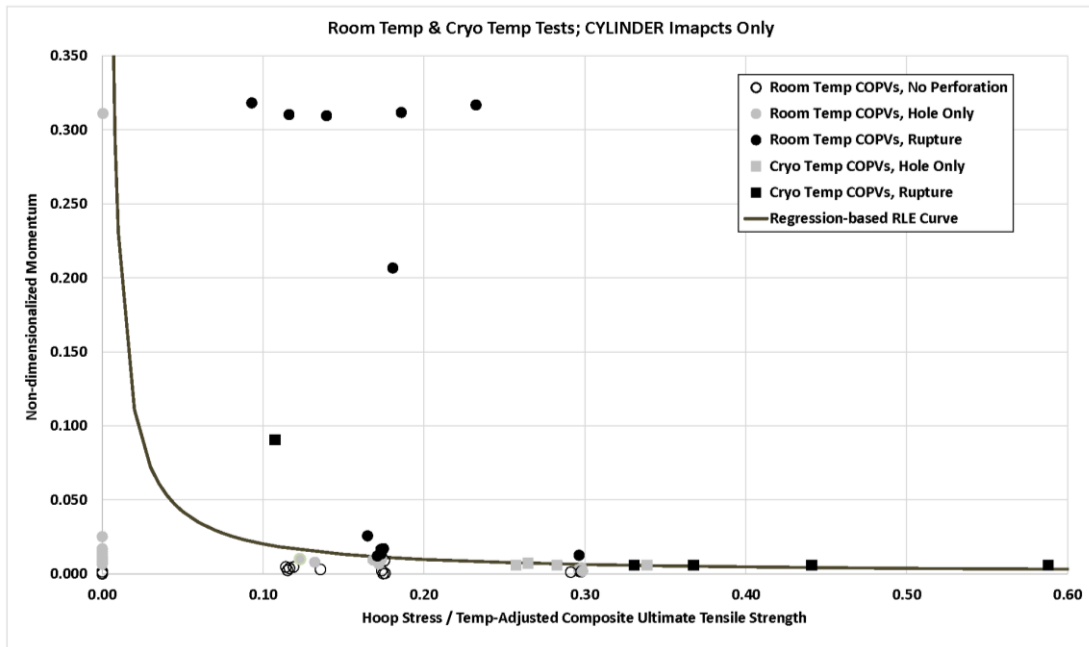


Figure 1. RLE Curve and Rupture/Non-rupture Data, Room Temperature and Cryogenic Temperatures

Linear scales were used in both Figure 1 and 2 to more clearly highlight the wide range of testing conditions that generated the data used in this study, as well as to make evident where additional testing might be warranted in future test programs. Comparison of the position of the RLE with the experimental results in Figures 1 and 2 shows that the RLE developed herein is, with only one exception, able to correctly separate regions of COPV rupture and non-rupture.

Further inspection of the regression-based RLE in Figures 1 and 2 reveals that this exception is where there appears to be a “flipped” placement of a non-rupture (grey data point) and a rupture (black data point) for two of the cryogenic tests (see Figure 2 near a non-dimensional hoop stress value of 0.33). The reason for this apparent inconsistency in the test data is not known at this time; however, it is not all unexpected in these types of test programs. Additional examples of the kinds of unexpected overlapping and unexpected “flipping” of, for example P/NP data, that frequently occur in these types of test programs can be seen in the various BLE development

plots in Christiansen (2003). But, other than those “flipped” data points, the RLE in Figures 1 and 2 appears to cleanly separate all the remaining R/NR data points.

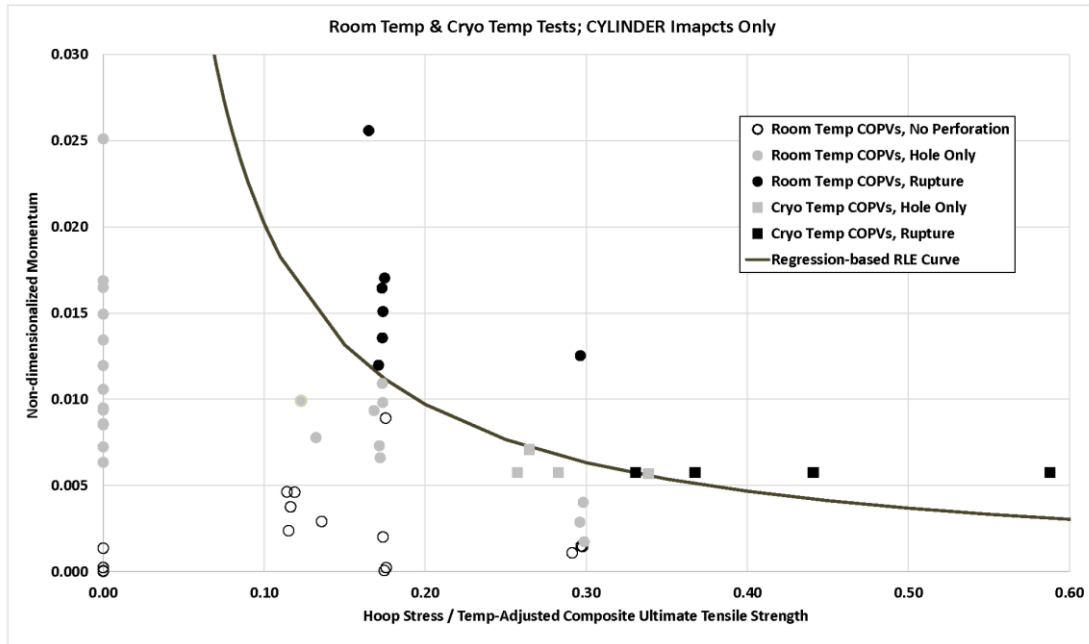


Figure 2. Zoomed in RLE Curve and Rupture/Non-rupture Data, Room Temperature and Cryogenic Temperatures

Also of note is that, because of the way the testing programs evolved, the data in Figures 1 and 2 appear to be grouped in six distinct clusters: horizontally near a non-dimensional momentum values of ~ 0.325 and ~ 0.006 , and vertically about non-dimensional hoop stress values of ~ 0 , ~ 0.12 , ~ 0.17 , and ~ 0.3 . Considering this distribution of the test data, further testing is needed so that additional data may be obtained in other regions in these figures (in particular, at higher internal pressure or hoop stress, and involving impact scenarios that result in non-dimensional impact momenta at ~ 0.2 and at ~ 0.1) to increase confidence in the RLE. Testing at other temperatures, as well as using other composite materials, would also help achieve increased confidence.

It is possible, of course, that future COPV testing could result in more overlap in the data. This would require some engineering judgement regarding where to best position the RLE so that program requirements are met. For example, if the program or project were to involve spacecraft in which humans were expected to live and work for long durations of time, it is highly likely that a more conservative design approach would be warranted. This would, in turn, dictate a “lower” placement of the RLE on plot grids such as those shown in Figure 1 and 2.

3.5 Conclusions

A study was performed to develop a single rupture limit equation (RLE) that would be an extension and combination of two previously-developed RLEs. This new RLE can now predict whether or not a COPV would rupture under a much wider range of temperatures, constructions, and COPV contents. The resulting RLE was able to correctly discriminate, in nearly all cases, those conditions that resulted in rupture of a COPV from those that did not. Additional testing is recommended, especially for impact and operating conditions near the current position of the

RLE, as well as perhaps for shielded COPVs (see, e.g., Telitchev, 2005, Telitchev, et al, 1997, and Telitchev, et al, 1999) as well as for other COPV contents (e.g., energetic fluids such as hydrazine – see Limido, et al, 2019, and Zhao, et al, 2019). The effects of other phenomenologies, such as hydraulic ram, on the impact response of the COPVs should also be addressed as the model is further refined. This testing and the data it produces would help enhance the ability of the RLE to predict the R/NR response of an even wider class of COPVs, operating conditions, and impact scenarios.

3.6 Acknowledgments

The author gratefully acknowledges the support provided by the NASA Engineering Safety Center and the NASA White Sands Test Facility for performing the impact tests that generated the data used herein.

3.7 References

- Avva, V.S. 1982. Impact Initiated Damage in Laminated Composites. AFOSR-TR-82-1038, Bolling AFB, Washington, DC.
- Avva, V.S. 1983. Fatigue/Impact Studies in Laminated Composites. AFWAL-TR-83-3060, Wright-Patterson AFB, Dayton, OH.
- Christiansen, E.L. 2003. Meteoroid/Debris Shielding. NASA TP-2003-210788, Johnson Space Center, Houston, TX.
- Davis, B.A., Christiansen, E.L., Miller, J.E., and Yoder, T.B. 2019. Hypervelocity Impact of Composite Overwrap Pressure Vessels. *Proceedings of the First International Orbital Debris Conference*, Paper No. 6102, Sugarland, TX.
- Garcia, M.A, Davis, B.A., and Miller, J.E. 2017. Hypervelocity Impact Testing of a Pressurized Composite Overwrapped Pressure Vessel and Comparison to Numerical Analysis. *Procedia Engineering*, Vol. 204, pp. 476–483.
- Hassin, J., Pfaab, K., Puillet, C., Limido, J., Lacome, J.L., and Hereil, P. 2015. Hypervelocity Impacts on Composite Overwrapped Pressurized Vessels. *Proceedings of DYNCOMP 2015*, Arles, France.
- Hereil, P.L., Mespoulet, J., and Plassard, F. 2015. Hypervelocity Impact of Aluminum Projectiles against Pressurized Aluminum-Composite Vessel. *Procedia Engineering*, Vol. 103, pp. 181-188.
- Lear, D.M., Christiansen, E.L., and Hyde, J.L. 2019. Bumper: A Tool for Analyzing Spacecraft Micrometeoroid and Orbital Debris Risk. *Proceedings of the First International Orbital Debris Conference*, Paper No. 6129, Sugarland, TX.
- Limido, J., Puillet, C., and Vila, J-P. 2019. Explosion of Hydrazine Tanks Due to Space Debris Impacts. *Proceedings of the 2019 Hypervelocity Impact Symposium*, Paper No. 99, Destin, FL, ASME, pp. 589-597.
- Nahra, H., Nichols, C., Christiansen, E., Greene, N., Miller, J., and Davis, B. 2015. Composite Over-wrapped Pressure Vessel (COPV) Hypervelocity Impact (HVI) Study: Phase – 1 (Impacts on Unpressurized COPV Tank), Johnson Space Center, Houston, TX.
- Schaefer, F., Schneider, E., and Lambert, M. 1997. An Experimental Study to Investigate Hypervelocity Impacts on Pressure Vessels. *Proceedings of the 2nd European Conference on Space Debris*, Darmstadt, Germany, Publication No. ESA-SP-393, pp. 435-443.

- Schonberg, W.P. 1993. Effect of Internal Stress Fields on the Perforation Response of Dual-Wall Structures under Hypervelocity Impact, *International Journal of Impact Engineering*, Vol. 14, pp. 637-646.
- Schonberg, W.P. 2016. Concise History of Ballistic Limit Equations for Multi-Wall Spacecraft Shielding. *Reviews of Human Space Exploration*, Vol. 1, pp. 46-54.
- Schonberg, W.P. 2018. Rupture of a Cryogenic Composite Overwrapped Pressure Vessel Following a High-Speed Particle Impact. *Aerospace*, Vol. 5, paper no. 20.
- Schonberg, W.P. 2020. Will it Leak? Will it Burst? COPV Perforation and Rupture after an MMOD Impact. *Journal of Space Safety Engineering*, Vol. 7, No. 3, 2020, pp. 213-221.
- Schonberg, W.P., and Ratliff, M. 2015. Hypervelocity Impact of a Pressurized Vessel: Comparison of Ballistic Limit Equation Predictions with Test Data and Rupture Limit Equation Development. *Acta Astronautica*, Vol. 115, pp. 400-406.
- Stepka, F.S. 1966. Projectile-Impact-Induced Fracture of Liquid-Filled, Filament-Reinforced Plastic or Aluminum Tanks. NASA TN-D-3456, NASA Lewis Research Center, Cleveland, OH.
- Telitchev, I.Y. 2005. Analysis of Burst Conditions of Shielded Pressure Vessels Subjected to Space Debris Impact. *Journal of Pressure Vessel Technology*, Vol. 127, pp. 179-183.
- Telitchev, I.Y., and Prokhorov, A.G. 1997. Damage Parameters Analysis of Shielded Pressure Vessels under Space Debris Impact. *Proceedings of the Second European Conference on Space Debris*, Darmstadt, Germany, Publication No. ESA-SP-393, pp. 435-443.
- Telitchev, I.Y., Schaefer, F., Schneider, W., and Lambert, M. 1999. Analysis of the Fracture of Gas-Filled Pressure Vessels under Hypervelocity Impact. *International Journal of Impact Engineering*, Vol. 23, pp. 905-919.
- Zhao, B., Zhao, J., Cui, C., and Wang, Y. 2019. Shock Initiation of a Satellite Tank under Debris Hypervelocity Impact. *Applied Sciences*, Vol. 9, paper no. 3957.

4.0 Rupture Limit Equation for Shielded COPVs

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Abstract

How well a spacecraft component resists the damage that might occur following a high-speed impact by a meteoroid or piece of orbital debris is one of the main design considerations for earth orbiting spacecraft. Most spacecraft have at least one pressurized tank on board, such as a liquid propellant tank. In addition to a hole, it is possible that, for certain tank designs, construction materials, operating conditions, and impact parameters, a pressurized vessel may experience catastrophic failure (i.e. rupture) as the result of such a high-speed impact. Risk assessments performed during the design phase of a spacecraft typically consider and weigh the likelihoods and consequences of various failures that might occur following a MOD particle impact, including whether or not such a tank rupture might occur. In this paper, the datasets from several test programs involving shielded and unshielded composite overwrapped pressure vessels (COPVs) are combined to yield a single, new equation that can be used for this purpose. Comparison with test data shows that this new equation is able to correctly discriminate between the COPV rupture and non-rupture data points for the full high-speed impact test database considered herein.

4.1 Introduction

One of the main design considerations for spacecraft in earth orbit is its protection against the damage that might occur following an on-orbit impact by a meteoroid or piece of orbital debris (MOD). How well a spacecraft component resists this damage, usually in the form of a hole that is created or crater that is too deep, is usually characterized by its ballistic limit equation (BLE). This empirical equation often predicts the size of the projectile that would cause failure (however it is defined) in terms of velocity, impact angle, projectile density, projectile shape, as well as the materials used in the impacted spacecraft structural system. A brief overview on the development of BLEs and how they are constructed for typical spacecraft systems can be found in Reference [1].

Most spacecraft have at least one pressurized tank on board, such as, for example, a liquid propellant tank. These tanks come in a variety of shapes (e.g., spherical, cylindrical, elliptical, etc), and are typically either metallic (aluminum, steel, or titanium), or overwrapped with a (relatively thin) composite material laminate. In addition to a hole, it is possible that, for certain tank designs, construction materials, operating conditions (e.g. internal pressure levels), and

impact parameters, a pressurized vessel may experience catastrophic failure (i.e. rupture) as the result of a hypervelocity impact. This was demonstrated in a spectacular fashion when an oxygen-filled Kevlar-overwrapped aluminum pressure vessel ruptured in an explosive fashion following the 6.4 km/s impact of a 3.18 mm aluminum sphere [2].

Risk assessments performed during the design phase of a spacecraft typically consider and weigh the likelihoods and consequences of various failures that might occur following a MOD particle impact. In this process, a variety of physical response-predictor equations is required. For spacecraft in low earth orbit, two such equations are the BLE (that predicts whether or not a perforation will occur), and a crater depth prediction equation (that predicts how deep into a relatively thick layer of material an impacting projectile will burrow). Another type of equation that is needed, specifically for pressurized vessels, is one that differentiates between regions of operating and impact conditions that, given a tank wall perforation, would result in either tank rupture (i.e., catastrophic failure) or only a relatively small hole. In a manner similar to a BLE, this equation is referred to as a Rupture Limit Equation (or RLE).

Two RLEs were recently developed – one for gas-filled cylindrical COPVs under hypervelocity impact at room temperature [3], and one for cylindrical COPVs filled with cryogenic fluids under similar impact conditions [4]. Most recently, the datasets from the room temperature gas-filled and the cryogenic liquid-filled test programs were brought together and used to develop a single RLE that was applicable across the full range of temperatures and filling fluids considered in the combined dataset [5]. In the test programs that provided the data that led to the development of these RLEs, the test articles did not have any shielding of any kind.

In these previous modelling efforts, a statistics-based method was used to determine the best placement of a curve between the two response regimes of interest (i.e. rupture and non-rupture). In this paper, the datasets from the (unshielded) room temperature gas-filled and the (unshielded) cryogenic liquid-filled testing programs are now combined with additional results from a test program involving COPVs having honeycomb sandwich plate (HC/SP) shields to develop a single, new RLE. This RLE is now applicable across the full range of operating conditions (i.e. pressurized and unpressurized, room temperatures and cryogenic temperatures, etc.), impact scenarios (i.e. steel and aluminum projectiles, impact velocities between 1.5 and 7.5 km/s, etc.), and COPV constructions (i.e., with or without shielding). Comparison with test data shows that the new RLE is able to correctly discriminate between the COPV rupture and non-rupture data points for the full high-speed impact test database considered herein.

Testing Program Overview

Data from 65 high-speed room temperature and cryogenic impact tests were combined with 8 tests on COPVs with honeycomb/sandwich panel (HC/SP) shields set off at a small distance from the COPVs. The aluminum HC/SP shields were 2 cm thick, and made with 1 mm thick Al 2024 face sheets; the HC/SP core specification is Hexcel 3/16-5056-0.0007. A blanket of multi-layer-insulation (MLI) with an areal density of 0.447 kg/m² was placed in front of the HC/SP panel. The MLI used consisted of one beta cloth 500GW layer (outside), and 9 layers Kapton with 9 separator layers of Dacron netting. The HC/SP used in these tests was similar to that used in [6].

Tables 1 and 2 provide an overview of impact test conditions and COPV geometries, respectively, in these tests. Additional information on the room temperature tests, i.e., those used in [3], can be found in [7-11]. Details regarding the cryogenic temperature tests used in [4] can

be found in [12], while details regarding the testing of the COPVs with HC/SP shields can be found in [13].

Table 1. Impact Test Conditions Overview

Parameter	Ref. [4]	Ref. [3]	This Study	Units
Projectile Material	Steel	AL 2017-T4, 440 SS	Aluminum	-----
Projectile Diameter	5.56	~1.0 – 8.0	4.0-6.0	mm
Trajectory Obliquities	0	0, 45, 60	0	deg
Impact Velocities	~1.7	~4 – ~7	~2.2 – ~6.8	km/s
Tank Contents	LN2	Air, GN2	Air, GN2, Water	-----
Tank Temperatures	72	~294 – ~327	~294	deg-K
Tank Pressures	~0.3 – ~1.2	0, 20,30, 50	0, 3, 9	MPa

Table 2. COPV Geometries and Material Properties

Parameter		Ref. [4]	Ref. [3]	This Study	Units
COPV Diameter (O.D.)		19	12 – 42	~20	cm
COPV Length (Cylinder)		46	30 – 90	N/A	cm
Composite	Fiber	Glass	T-1000	Graphite	-----
	Matrix	Epoxy	LRF092, HARF53, SIREZ	Epoxy	-----
	Thickness	0.3 – 1.0	2.54 – 6.95	0.85-3.05	mm
Liner	Material	Aluminum	AL 6061-T6, Inconel	Aluminum	-----
	Thickness	0.127	0.75 – 2.3	0.8-1.35	mm
Shieldin	Type	None	None	HC/SP ^a	-----
	Stand-off Distance	N/A	N/A	10, 20	cm
	Areal Density	N/A	N/A	4.024	kg/m ²

^a 2 cm thick, 1 mm thick Al 2024 face sheets, core specification is Hexcel 3/16-5056-0.0007; MLI blanket with an areal density of 0.447 kg/m² placed in front of the HC/SP shield

4.2 Rupture Limit Equation Development

Using the formulation originally proposed in [14] and later used in [3-5], a simple power law was chosen to relate the ordinate (non-dimensional projectile momentum) to the abscissa (non-dimensional hoop stress) in a manner that would delineate between regions of rupture and non-rupture. To include the effects of the HC/SP shielding, the non-dimensional form of projectile momentum developed in [5] was adjusted by including some additional dimensionless terms, with this final result:

$$\frac{m_{proj} v_{proj}^{norm}}{(\rho_{comp} t_{tot}^3)^{\frac{1}{2}} \sqrt{\frac{\sigma_{ult}^{comp}}{\rho_{comp}}}} \left(\frac{\rho_{proj}}{\rho_{comp}} \right)^Q \left(\frac{\tau}{294.44 \text{ } ^\circ K} \right)^R \left(1 + \frac{\sigma_{ult}^{liner}}{100 \text{ MPa}} \right)^S \left(1 + \frac{\sigma_{yld}^{liner}}{100 \text{ MPa}} \right)^T \left(\frac{\rho_{air}}{\rho_{cont}} \right)^U$$

$$x \left(1 + \frac{10 \rho_{areal}^{HC/SP}}{D_{cyl} \rho_{proj}} \right)^V \left(1 + \frac{S}{d_{proj}} \right)^W = A \left(\frac{\sigma_{hoop}}{\sigma_{ult}^{comp}} \right)^B \quad (1)$$

where σ_{hoop} and σ_{ult}^{comp} are the COPV hoop stress and the composite material uni-directional temperature-adjusted ultimate stress (or maximum in-plane tensile stress), respectively, S is the stand-off distance between the HC/SP shield and the COPV, $\rho_{areal}^{HC/SP}$ is the areal density of the HC/SP shield (including the MLI blanket), D_{cyl} is the nominal diameter of the cylindrical portion of the COPV, and ρ_{proj} and d_{proj} are the density and diameter, respectively, of the impacting projectile. The hoop stress was calculated using the standard relationship for cylindrical pressure vessels:

$$\sigma_{hoop} = \frac{p_{int} r_{OD}}{t_{tot}} \quad (2)$$

where $t_{tot} = t_{comp} + t_{liner}$ is the total COPV wall thickness at the center of the cylindrical portion of the COPV, p_{int} is the COPV internal pressure, and r_{OD} is the outer radius of the COPV. Furthermore, V_{proj}^{norm} is the normal component of impact velocity, σ_{ult}^{liner} and σ_{yld}^{liner} are temperature-adjusted liner material ultimate and yield stresses, respectively, in MPa, τ is the test or COPV contents temperature in °K, and ρ_{air} , ρ_{comp} , and ρ_{cont} are the densities of air at room temperature, the composite overwrap material, and the COPV contents, respectively.

The forms of the non-dimensionalized hoop stress and impact momentum in Eqn (1) were selected so as to both characterize impact loading and internal pressurize and include material parameters that differed among the test programs pooled together to develop the more general RLE being proposed herein. The values of the yield stress and ultimate stress for the liner material were divided by 100 MPa, and the areal density of the HC/SP multiplied by 10, in order that these terms would not be overly larger or smaller than the values of the other terms in the equation. Values of yield stress, density, etc. for the materials listed in Table 2 and used in Eqn (1) were obtained from various online material properties databases.

The exponents Q through W in Eqn (1) were manually selected to facilitate the separation between rupture and non-rupture test results, which in turn lead to an a RLE that would be positioned between those two regions. If additional tests results were to become available, this approach would allow modification of the exponents Q through W in Eqn (1) to incorporate whatever new R/NR were to become available.

Another important advantage of this particular type of formulation is that it is not necessary to know the precise stacking sequence of the composite overwrap laminate in order to be able to use the RLE being developed; all that is required is the uni-axial temperature-adjusted ultimate tensile strength of the composite material. This also increases the potential applicability of this RLE to a much wider family of COPV constructions.

The values of these exponents Q through W were determined to be $Q = 0.1$, $R = 0.3$, $S = T = -1.0$, $U = 1.22$ (without a HC/SP shield), $U = 0.42$ (with a HC/SP shield), $V = -17.2$, and $W = -0.82$. The constants A and B in Eqn (1) were subsequently determined through a regression analysis of the rupture/non-rupture data. This process is also described in detail in [3,4]. Following this approach yields $A = 1.805 \times 10^{-3}$, $B = -1.044$ (correlation coefficient of 69.2%).

4.3 Comparison of the Rupture Limit Equation against R/NR Test Data

Plots of the RLE developed herein and corresponding empirical rupture/non-rupture test results are shown in Figures 1 and 2; Figure 1 is a plot of all the data, while Figure 2 is a “zoomed-in” version of Figure 1. In Figures 1 and 2, the solid black data points denote tank, the grey data points are tests that did not result in rupture but where a perforation of the COPV did occur, and the hollow data points indicate non-perforated tests. Comparison of the position of the RLE with the experimental results in Figures 1 and 2 shows that the RLE developed herein is, with only one exception, able to correctly separate regions of COPV rupture and non-rupture.

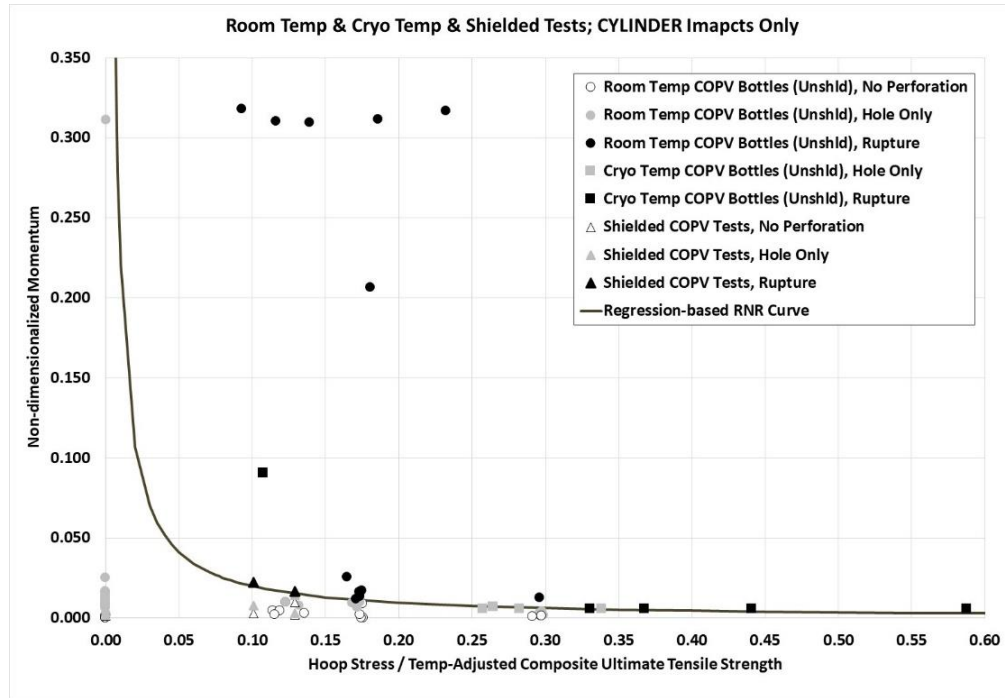


Figure 1. RLE Curve and Rupture/Non-rupture Data, Room and Cryogenic Temperatures, Shielded and Unshielded COPV

As discussed in [5], this exception appears where a non-rupture (grey data point) and a rupture (black data point) for two of the cryogenic tests appear to be “flipped” (see Figure 2 near a non-dimensional hoop stress value of 0.33). These kinds of “flipped” data points are not at all uncommon in high-speed impact test programs (see, e.g., [15]). Other than those two “flipped” data points, the RLE in Figures 1 and 2 appears to cleanly separate all the remaining R/NR data points.

It is possible, of course, that future COPV testing could result in more overlap in the data. This would leave open the question as to where would be the most appropriate position of the RLE. In such cases, it is entirely likely that program requirements might dictate whether the RLE would be placed “lower” among the data, thereby rendering a more conservative prediction of whether or not rupture might occur, or “higher” among the data, if a less conservative prediction model would be acceptable.

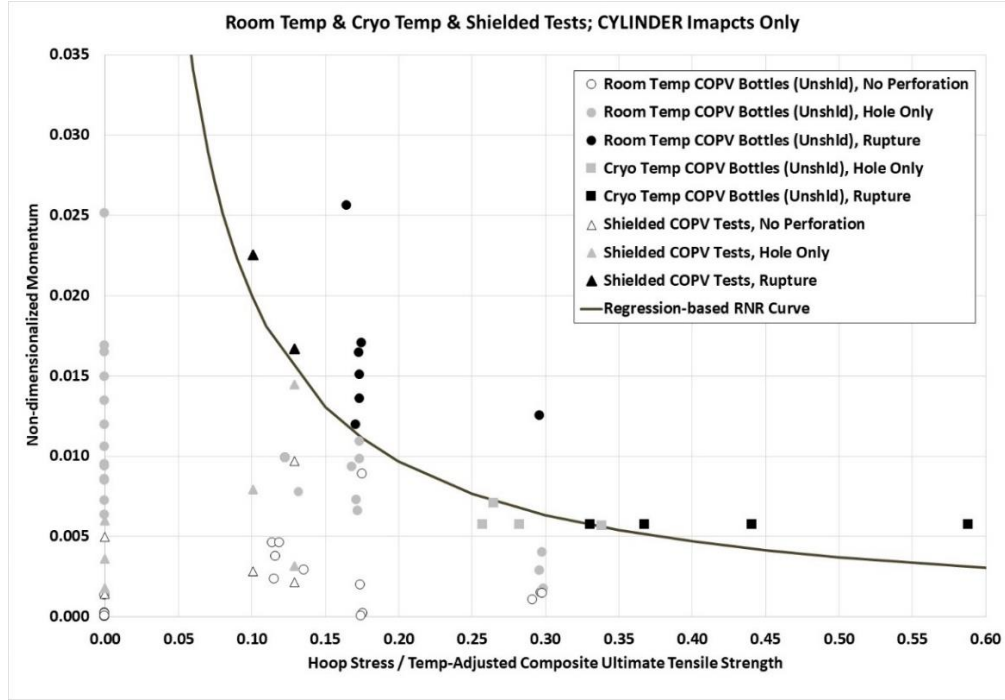


Figure 2. Zoomed in RLE Curve and Rupture/Non-rupture Data, Room and Cryogenic Temperatures, Shielded and Unshielded COPVs

4.4 Adjustments and Updates to the Equations

It is important to note that the RLE developed in this paper (as well as the RLEs developed previously [3,4,5], the BLE developed previously [3], and hole area predictor equations developed previously [16]) are calibrated against the nominal COPV liner material properties given in [3]. However, as noted herein, in addition to being readily adjustable if additional tests were to become available, the coefficients in these equations can also be adjusted to reflect any changes in material properties against which these equations are calibrated.

For example, consider the case where it might be desirable to re-cast these equations using the actual material properties of the aluminum 6061-T6 liner (i.e., $\sigma_{ult}^{liner} = 310$ MPa, $\sigma_{yld}^{liner} = 276$ MPa) or the Inconel liner (i.e., $\sigma_{ult}^{liner} = 1375$ MPa, $\sigma_{yld}^{liner} = 1100$ MPa) rather than the nominal ones listed in [3]. Such a modification can be readily accomplished to reflect the new placements of the data points in Figures 1 and 2 that would result from the actual material property values being used in the non-dimensional formulations of momentum and hoop stress as appropriate. Tables 3-7 below compare the values of the coefficients in the various RLE, BLE, and hole predictor equations (as well as the correlation coefficients associated with the regression exercise performed in their development) when nominal material property values for aluminum 6061-T6 and Inconel are used and when actual material property values are used.

Table 3. Comparison of Room Temperature Rupture Limit Equation (RLE) Coefficients

Coefficient or Exponent	Nominal Material Properties [3]	Actual Material Properties
Q	0.1	0.1
S	-1.0	-1.0
T	-1.0	-1.0
Corr Coeff	74.3%	75.7%
A	2.166×10^{-3}	2.968×10^{-3}
B	-0.943	-0.917

Table 4. Comparison of Room Temperature Ballistic Limit Equation (BLE) Coefficients

Coefficient or Exponent	Nominal Material Properties [3]	Actual Material Properties
P₀	6.00×10^{-3}	7.00×10^{-3}
C	4.00	11.7
E	7.35	11.7

Table 5. Comparison of Hole Area Predictor Equation Coefficients

Coefficient or Exponent	Composite Overwrap Equation		Liner Equation	
	Nominal Material Properties [16]	Actual Material Properties	Nominal Material Properties [16]	Actual Material Properties
A	20.514	11.202	0.136	0.111
B	2.382	2.412	-9.334	-8.220
C	0.794	0.703	0.614	0.695
Corr Coeff	78.2%	76.3%	92.7%	92.7%

Table 6. Comparison of Room + Cryo Temperature Rupture Limit Equation (RLE) Coefficients

Coefficient or Exponent	Nominal Material Properties [5]	Actual Material Properties
Q	0.1	0.1
R	0.3	0.3
S	-1.0	-1.0
T	-1.0	-1.0
U	1.22	1.16
Corr Coeff	70.0%	70.5%
A	1.769×10^{-3}	2.496×10^{-3}
B	-1.058	-1.030

Table 7. Comparison of Rupture Limit Equation (RLE) Coefficients for Shielded COPVs

Coefficient or Exponent	Nominal Material Properties	Actual Material Properties
Q	0.1	0.1
R	0.3	0.3
S	-1.0	-1.0
T	-1.0	-1.0
U (without shielding)	1.22	1.155
U (with shielding)	0.42	0.42
V	-17.2	-18.0
W	-0.82	-0.68
Corr Coeff	69.2%	69.7%
A	1.805×10^{-3}	2.516×10^{-3}
B	-1.044	-1.023

As can be seen in these tables, the values of the coefficients in the various COPV response predictor equations developed recently can change (but usually not by very much) if material properties other than the nominal ones are used in the development of these equations. However, the correlation coefficients for the regression exercises used to obtain the power-law coefficients and exponents remain equally high when the actual material property values, for example, of aluminum 6061-T6 and Inconel, are used. Plots corresponding to those shown in Figures 1 and 2 for the various datasets noted in Tables 3-7 show an identical ability to discriminate between “event” and “non-event” regions when actual material properties are used in equation development as when nominal property values are used. This indicates that the method developed herein is highly versatile and can be adapted to changes in the information it uses to develop the various equations of interest.

4.5 Conclusions

A study was performed to develop a single rupture limit equation (RLE) that would be an extension and combination of several previously-developed RLEs. This new RLE can now predict whether or not a COPV would rupture under a much wider range of temperatures, constructions, and COPV contents. The resulting RLE was able to correctly discriminate, in nearly all cases, those conditions that resulted in rupture of a COPV from those that did not. Additional testing is recommended, especially for impact and operating conditions near the current position of the RLE where data does not yet exist, as well as for other COPV contents (e.g., common spacecraft fuels such as hydrazine). This testing and the additional data would increase the applicability of this type of RLE to an even wider class of COPVs, operating conditions, and impact scenarios.

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4.7 References

1. Schonberg, W.P. 2016. "Concise History of Ballistic Limit Equations for Multi-Wall Spacecraft Shielding." *Reviews of Human Space Exploration*, Vol. 1, pp. 46-54.
2. Rucker, M.A., Deeson, H., Stoltzfus, J., and Benz, F. 1991. Demonstration of a Hypervelocity Impact Test Capability, NASA WSTF TR-692-001, Johnson Space Center, Houston, TX.
3. Schonberg, W.P. 2020. "Will it Leak? Will it Burst? COPV Perforation and Rupture after an MMOD Impact." *Journal of Space Safety Engineering*, Vol. 7, No. 3, pp. 213-221.
4. Schonberg, W.P. 2018. "Rupture of a Cryogenic Composite Overwrapped Pressure Vessel Following a High-Speed Particle Impact." *Aerospace*, Vol. 5, paper no. 20.
5. Schonberg, W.P. 2021. "Predicting the Rupture of a COPV Impacted by a High-Speed Orbital Debris Particle: Including the Effects of Temperature and COPV Contents." *ASCE Journal of Aerospace Engineering* (in press).
6. Lambert, M., Schaefer, F., and Geyer, T. 2001. "Impact Damage on Sandwich Panels and Multi-Layer Insulation." *International Journal of Impact Engineering*. Vol. 26, pp. 369 - 380.
7. Davis, B.A., Christiansen, E.L., Miller, J.E., and Yoder, T.B. 2019. "Hypervelocity Impact of Composite Overwrap Pressure Vessels." *Proceedings of the First International Orbital Debris Conference*, Paper No. 6102, Sugarland, TX.
8. Garcia, M.A, Davis, B.A., and Miller, J.E. 2017. "Hypervelocity Impact Testing of a Pressurized Composite Overwrapped Pressure Vessel and Comparison to Numerical Analysis." *Procedia Engineering*, Vol. 204, pp. 476–483.
9. Hassin, J., Pfaab, K., Puillet, C., Limido, J., Lacome, J.L., and Hereil, P. 2015. "Hypervelocity Impacts on Composite Overwrapped Pressurized Vessels." *Proceedings of DYNCOMP 2015*, Arles, France.
10. Hereil, P.L., Mespoulet, J., and Plassard, F. 2015. "Hypervelocity Impact of Aluminum Projectiles against Pressurized Aluminum-Composite Vessel." *Procedia Engineering*, Vol. 103, pp. 181-188.
11. Nahra, H., Nichols, C., Christiansen, E., Greene, N., Miller, J., and Davis, B. 2015. *Composite Over-wrapped Pressure Vessel (COPV) Hypervelocity Impact (HVI) Study: Phase – 1 (Impacts on Unpressurized COPV Tank)*, Johnson Space Center, Houston, TX.
12. Stepka, F.S. 1966. *Projectile-Impact-Induced Fracture of Liquid-Filled, Filament-Reinforced Plastic or Aluminum Tanks*. NASA TN-D-3456, NASA Lewis Research Center, Cleveland, OH.
13. Schafer, F. K., Putzar, R., and Lambert, M. 2008. "Vulnerability of Satellite Equipment to Hypervelocity Impacts." *Proceedings of the 59th IAC*, Paper No. IAC-08-A6.3.2, Glasgow, Scotland.
14. Schonberg, W.P., and Ratliff, M. 2015. "Hypervelocity Impact of a Pressurized Vessel: Comparison of Ballistic Limit Equation Predictions with Test Data and Rupture Limit Equation Development." *Acta Astronautica*, Vol. 115, pp. 400-406.
15. Schonberg, W.P., and Compton, L.E. 2008. "Application of the NASA/JSC Whipple Shield Ballistic Limit Equations to Dual-Wall Targets under Hypervelocity Impact." *International Journal of Impact Engineering*, Vol. 35, pp. 1792-1798.
16. Schonberg, W.P. 2008. "Non-catastrophic Perforation of a Composite Overwrapped Pressure Vessel." *Acta Astronautica*, Vol. 174, pp. 41-47.

5.0 Non-Catastrophic Perforation of a Composite Overwrapped Pressure Vessel

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Abstract

Most spacecraft have a pressurized tank on board, such as a liquid propellant tank or a breathable air supply tank. Because of the serious damage that might result following an on-orbit micro-meteoroid or orbital debris (MMOD) particle impact, a primary design consideration of such (often highly) pressurized tanks is the mitigation of the damage that might occur in the event of such an impact. While catastrophic failure (i.e. rupture) following a high-speed MMOD particle impact would be disastrous, a puncture and the resulting thrust caused by the expulsion of fluids or gas from the perforated tank could result in the destabilization of the spacecraft's orbit which could, in turn, also lead to loss of the spacecraft. As such, in the event that catastrophic failure does not occur, risk assessments must still consider whether or not a perforation of a pressurized tank might occur and, in the event of a perforation, what size hole is created by the impact. This paper presents the development of a ballistic limit equation for highly pressurized COPVs that would differentiate between combinations of impact parameters and operating conditions that would result in a hole (without rupture) in the front side of the impacted pressure vessel from those that would not. Empirical models for the hole-out area in the composite overwrap and in the interior liner material in the event of a front side perforation are also developed and discussed.

5.1 Introduction

Most spacecraft have at least one pressurized vessel on board, such as a liquid propellant tank or a breathable air supply tank. If the spacecraft is operating in earth orbit, such tanks are necessarily exposed to the hazards of the micro-meteoroid and orbital debris (MMOD) environment. Because of the potential of serious mission-threatening damage that might result following an on-orbit MMOD particle impact, one of the primary design considerations of such spacecraft is the anticipation and mitigation of the possible damage that might occur in the event of such an impact. To prevent mission failure and possibly loss of life, protection against perforation of such tanks by high-speed impacts must be included in the design of the spacecraft.

Considerable energy and effort has been expended in the study of the response of non-pressurized spacecraft components to MMOD-like impacts. However, fuel tanks are pressurized internally, and so their main walls will develop bi-axial stress fields because of that internal pressurization. The extent of the damage caused by a high-speed impact on a pressurized vessel

can range from surface pitting or cratering, to perforation (of either just the “front side” of the tank or of both the “front” and “rear” sides), and ultimately to catastrophic failure (or rupture). Until recently, technical and safety challenges have limited the number of tests that have been conducted on highly pressurized spacecraft elements. Reference [1] presents a summary of the high-speed impact testing that has been done using pressurized tanks (most of which were metallic) from the early 1960s through the early 2000s.

Since then, in order to study and increase our understanding of the effects of internal pressure on the high-speed impact response of pressure vessels, several high-speed impact test programs have been conducted using a variety of pressurized tanks. These tests have been performed with varying amounts of internal pressure (including none); with internal fluids, air, or at a vacuum; and again, mostly using (spherical) metallic tanks (see again [1]). However, the number of hypervelocity impact tests performed using composite overwrapped pressure vessels (COPVs) has continued to remain especially limited. Reference [2] summarizes some of the early work involving pressurized COPVs, most of which was done with tanks pressurized with cryogenic fluids.

In an effort to address the lack of information on the response of pressurized COPVs under high-speed impact conditions, several testing programs have recently been undertaken to generate data that could be used to study and characterize the hypervelocity impact response of COPVs. These included the impact of shielded (see, e.g., [3,4]) as well as unshielded COPVs (see, e.g., [5-10]). In Ref. [7], impact test data from several different unshielded COPV impact studies were pooled together for the purpose of developing a COPV “rupture limit equation”, or RLE. In a manner similar to a ballistic limit equation, or BLE, the RLE developed in [7] is able to predict whether or not a particular set of impact parameters under a specified operating condition would result in the catastrophic failure, or rupture, of metallic pressure vessels and COPVs under high-speed impact loadings.

During the test program that generated the data used in the development of the COPV RLE [7], as projectile diameter was increased while impact velocity was kept (relatively) constant, it was observed that before projectiles were large enough to cause rupture there were impact conditions that resulted in merely front side perforation of the COPV (even under highly pressurized conditions). While catastrophic failure (i.e. a rupture) as a result of a high-speed MMOD particle impact would be disastrous to a mission and to the spacecraft occupants (if any), a perforation or puncture and the resulting thrust caused by the expulsion of fluids or gas from the perforated tank could result in the de-stabilization of the spacecraft’s orbit. This, in turn, could also lead to loss of the spacecraft and quite possibly, for human missions, loss of life. The question of orbit de-stabilization and global dynamic structural response following an MMOD impact was initially addressed in a study that considered impact conditions that would result in the perforation of one of the Space Station Freedom’s habitable modules [11].

Risk assessments performed during the design phase of a spacecraft typically consider and weigh the likelihoods and consequences of various failure modes that might occur following an on-orbit MMOD impact. A rupture limit equation such as that developed recently is therefore required to discriminate between combinations of impact parameters and operating conditions that may or may not lead to a catastrophic failure event. However, in the event that catastrophic failure does not occur, it is still important to know whether or not a perforation of the pressurized tank might occur.

Whether or not a structural element is perforated by a hypervelocity impact is typically characterized by a ballistic limit equation (BLE). This equation is traditionally derived from data-based curves which are drawn to distinguish between regions of perforation and non-perforation (P/NP) in terms of impact parameters and spacecraft element configuration materials, geometries, etc.

Furthermore, in the event that perforation does occur, it is also important to know what is the size of the hole that is created. This information could be used in a risk assessment to determine whether it is large enough so that the thrusting force on the spacecraft created by the exhausting fluid or gas would de-stabilize the orbit of the spacecraft. In this case, another type of equation is needed – one that can be used to calculate the hole-out area in a perforated pressure vessel.

This paper presents the results of a two-part effort that was part of the testing and analysis program described in Refs. [8-10]. The first task was directed at the development of a data-driven BLE for highly pressurized COPVs that would differentiate between combinations of impact parameters and operating conditions that would result in a hole in the front side of the impacted pressure vessel from those that would not. The second part of the effort was aimed at developing empirical models for hole-out area in the composite overwrap and in the interior liner material in the event of a front side perforation. When complete, this entire suite of equations (the RLE, the BLE, and the two hole-out area equations) can enhance the capabilities of a spacecraft probabilistic risk assessment by allowing it to consider a number of possible failure scenarios that have heretofore not been considered.

5.2 Test Parameters Overview

In this study, we focus on the high-speed impact tests performed on unshielded composite material overwrapped pressure vessels (COPVs) filled with pressurized air. Data from approx. 40 high speed impact tests on four different types of cylindrical COPVs are pooled together and used in the development of the BLE and the empirical hole-out area predictor equations. Tables 1 and 2 provide a summary of the impact test conditions and also the geometric parameters and material properties of the COPVs used in these impact tests. Additional information regarding these tests can be found in Refs. [8-10].

Table 1. Overview of COPV Impact Test Conditions

Parameter		Type I		Type II		Type III, IV		Units
Projectile	Material	Al 2017-T4	440C SS	Al 2017-T4	440C SS	Al 2017-T4	440C SS	
	Density ^a	2.79	7.63	2.79	7.63	2.79	7.63	gm/cm ³
	Diameter	1.0 – 2.5	1.09, 1.29	2.71, 3.40, 5.01	1.70 – 1.99	1.00 – 1.30	0.090 – 0.129	mm
Trajectory	Obliquity	0, 45, 60	45	0, 45	45	45	45	deg
	Velocity	~4.09, 7.05	~7.06	~7.00	~7.00	~7.00	~7.00	km/s
Impact Location		Cylinder		Cylinder		Cylinder		
Contents		Air		Air		Air		
Pressure		0, ~29		~31		~21		MPa

^aNominal values from www.matweb.com

5.2.1 Comments on the Testing

It is important to note that only tests in which the projectile impacted the cylindrical portion of the COPV were considered in the development of the BLE and the hole-out predictor equations. While a number of the tests in Refs. [8-10] were conducted with impacts occurring on the shoulder or hoop-to-dome-transition region (the so-called HTDTR of the COPV), these tests were not included because of the complexity of the stress fields in this region. While hoop stresses in the cylindrical portion of a COPV could be reasonably approximated using fundamental mechanics-based equations, the dependence of the HTDTR hoop stress on dome shape as well as internal pressure, thickness, etc., precluded the development of a simple equation from being developed and used in subsequent analysis and curve development.

Table 2 Geometric Parameters and Material Properties of COPVs

Parameter		Type I	Type II	Type III	Type IV	
COPV Diameter (O.D)		16.75	42.5	12.7	21.1	cm
COPV Length (Cylinder)		34.3	54.6	29.2	25.4	cm
Composite	Fiber	T1000	T1000	T1000	T1000	
	Matrix	LRF-092	HARF-53	SIREZ	SIREZ	
	Tensile Strength ^a	3040	3040	3040	3040	MPa
	Density ^a	1.56	1.56	1.56	1.56	gm/cm ³
	Thickness ^b	2.54	6.35	3.64	3.64	Mm
Liner	Material	Al 6061-T6	Inconel	Al 6061-T6	Al 6061-T6	
	Tensile Strength ^c	455	1282	455	455	MPa
	Shear Strength ^c	273	882	273	273	MPa
	Yield Stress ^c	285	1041	285	285	MPa
	Density ^c	2.70	8.19	2.70	2.70	gm/cm ³
	Thickness ^d	1.98	0.81	1.52	1.70	mm

^aNominal value from manufacturer's literature

^bNominal composite thickness in cylindrical portion of COPV

^cNominal values from www.matweb.com

^dNominal liner thickness in cylindrical portion of COPV

5.3 Ballistic Limit Equation Development

The empirically-based ballistic limit equation (BLE) was developed using the impact response data from Refs. [8-10]. To render the equation as broadly applicable as possible, operating conditions (x-axis) were parameterized as the hoop stress in the tank (non-dimensionalized by the uni-axial ultimate tensile stress of the composite overwrap material), while impact conditions (y-axis) were parameterized as impact momentum (non-dimensionalized by a number of tank wall material properties). This approach was used previously to successfully model the rupture/non-rupture response of cylindrical and spherical metallic tanks [1], as well as pressurized COPVs at cryogenic [3] and room-temperature [7] operating conditions. The non-dimensional form of projectile momentum is thus written as follows:

$$\text{Non-dimensional Projectile Momentum} = \frac{m_{proj} v_{proj}^{norm}}{(\rho_{comp} t_{tot}^3) \sqrt{\frac{\sigma_{ult}^{comp}}{\rho_{comp}}}} \left(\frac{\rho_{proj}}{\rho_{comp}} \right)^Q \left(1 + \frac{\sigma_{ult}^{liner}}{100} \right)^S \left(1 + \frac{\sigma_{yld}^{liner}}{100} \right)^T \quad (1)$$

where $t_{tot} = t_{comp} + t_{liner}$ is the total nominal thickness of the cylindrical portion of the composite material overwrap, σ_{ult}^{comp} is the uni-directional ultimate stress of the COPV composite material, V_{proj}^{norm} is the normal component of the impact velocity, and σ_{ult}^{liner} and σ_{yld}^{liner} are the ultimate and yield stresses of the metallic liner material, respectively, in MPa.

The first term in the denominator in Eqn (1) has units of mass while the second has units of velocity, thereby rendering the right-hand-side of Eqn (1) unitless, or non-dimensional, so long as there is consistency in the units of mass and velocity used in its numerator and denominator. The terms with exponents Q , S , and T were added to include the effects of other material parameters that might affect COPV impact response, such as liner yield strength and ultimate strength.

The particular forms of the non-dimensionalized hoop stress and impact momentum in Eqn (1) were motivated by the desire to include those terms that not only characterized the impact loads and internal pressurization conditions, but at the same time included material parameters that were seen to differ among the four COPV types as noted in Table 2. It is also important to note that the ability to use this equation does not rely on having to know the exact lay-up or stacking sequence of the composite laminae in the COPV – all that is needed is the uni-axial ultimate strength of the material. This serves to increase the ease of use of this equation and its applicability to a wider family of COPV constructions.

The exponents Q , S , and T in Eqn (1) were selected so as to allow, as much as possible, a natural separation between perforation and non-perforation test data points when plotted on x- and y-axes as described above. This would, in turn, facilitate the development of a BLE that would, also as much as possible, lie between those two regions. The attractiveness and benefit of this approach is that if additional tests results were to become available, the exponents Q , S , and T in Eqn (1) could again be adjusted to allow the incorporation of whatever new perforation/non-perforation data becomes available and the subsequent development of a revised BLE.

Figure 3 shows the raw COPV perforation/non-perforation data when plotted on non-dimensional hoop stress and impact momentum axes (i.e. when $Q = S = T = 1.0$). It is seen that the perforation data points (red) and widely scattered amongst and intermingled with the non-perforation data points (blue). However, as the values of Q , S , and T are adjusted a natural separation among the red and blue data points was seen to emerge. The optimal separation was found to occur when the values of the exponents Q , S , and T used in the non-dimensionalization scheme defined in Eqn (1) were $Q = 0.1$, and $S = T = -1.0$. Figure 4 shows the same data as in Figure 3, except now with these value of Q , S , and T introduced in to the expression for non-dimensional projectile momentum.

As can be seen in Figure 4, this non-dimensionalization scheme together with the chosen exponent values of Q , S , and T resulted in all but one of the perforation data points being grouped separately from the non-perforation test results. This allowed for the fairly straightforward development of a simple BLE for the COPVs considered in this study, based on the test data obtained thus far. A discussion of the non-perforation test result that occurred when a perforation result might have been expected is deferred for a subsequent section.

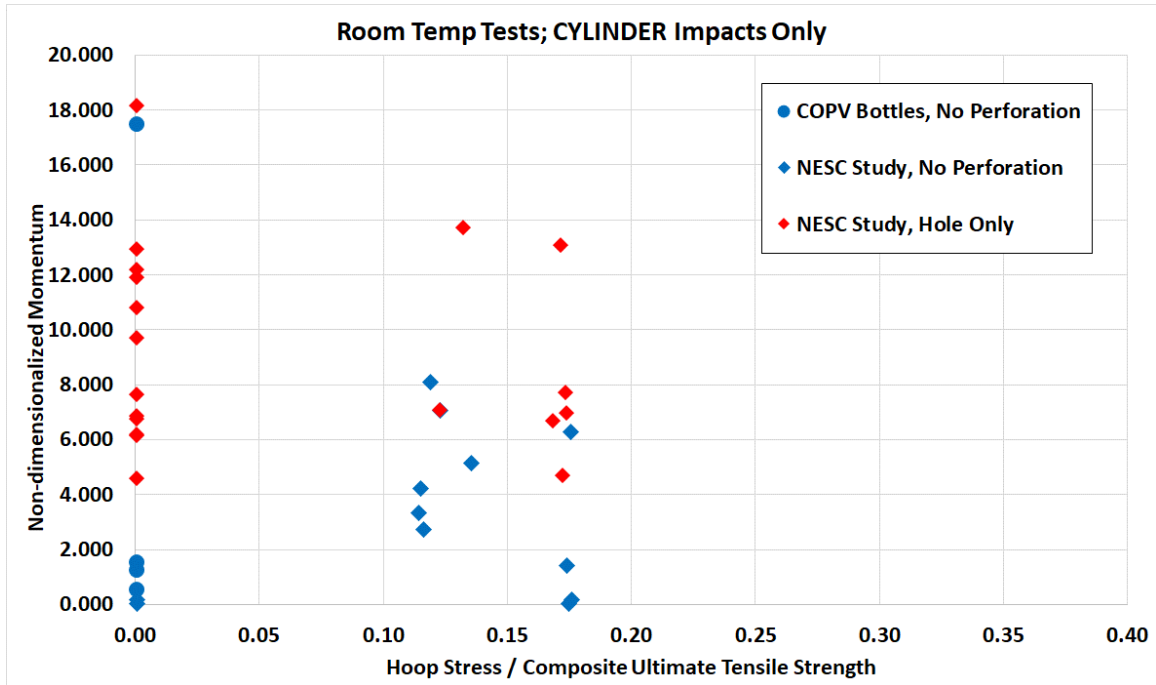


Figure 3. Perforation/Non-Perforation Data ($Q = S = T = 1.0$)

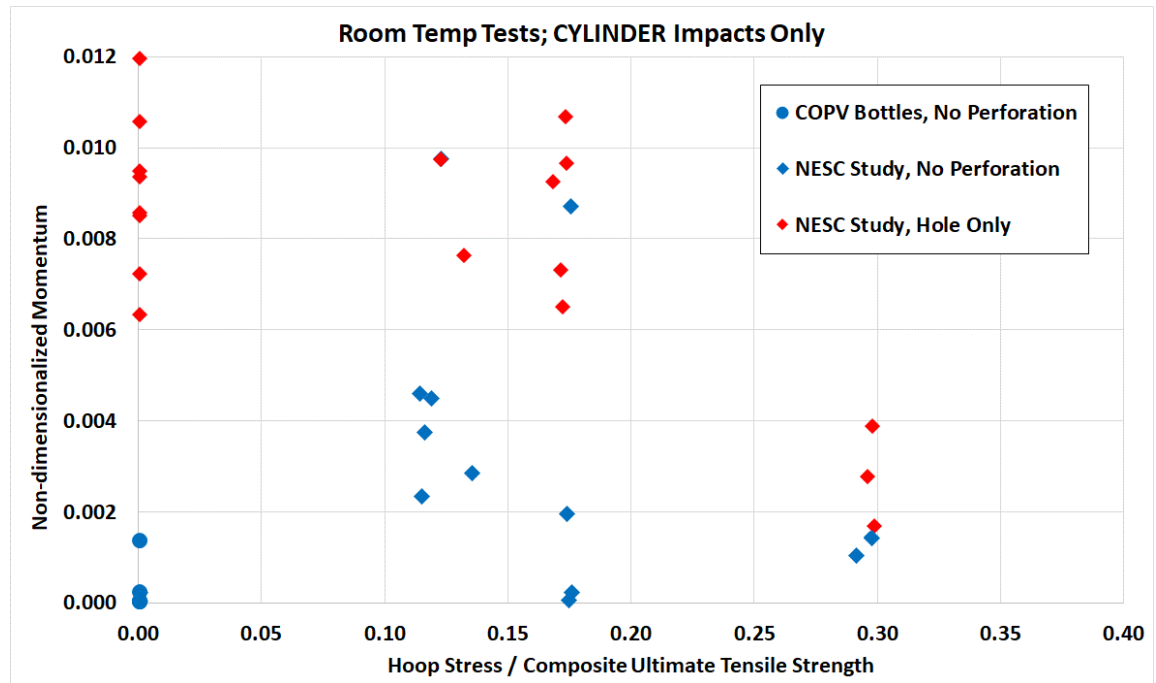


Figure 4. Perforation/Non-Perforation Data ($Q = 0.1; S = T = -1.0$)

Considering, then, the rather clean separation of the perforation and non-perforation data points that was obtained with the judicious selection of the values of the exponents Q , S , and T , we are now ready construct a ballistic limit equation (BLE) for the COPVs considered in this study. This equation can then be used to predict the onset of “front face” perforation of such similar COPVs in the event of an on-orbit MMOD particle impact.

5.3.1 COPV BLE Function

The COPV BLE was obtained by determining the coefficients of a relatively simple exponential function that, with regard to the blue and red data points in Figure 4, would (1) lie between the highest blue and lowest red data points when the non-dimensional hoop stress was ~ 0.127 , ~ 0.173 , and ~ 0.297 , and (2) approach zero as non-dimensional hoop stress approached unity. This functional form is given by Eqn (2) as follows:

$$\text{Non-dimensional Projectile Momentum} = A + Be^{C(\sigma_{hoop}/\sigma_{ult}^{comp})} \quad (2)$$

where the non-dimensional projectile momentum in Eqn (2) is given by Eqn (1), the associated values of the coefficients Q , S , and T are as noted previously, and

$$\sigma_{hoop} = \frac{p_{int}r_{OD}}{t_{tot}} \quad (3)$$

where p_{int} is the internal pressure in the COPV and r_{OD} is the COPV outer radius. Enforcing condition (2) above yields the following relationship between the three coefficients A , B , and C :

$$A = -Be^C \quad (4)$$

Substituting Eqn (4) into Eqn (2) and combining terms yields the following revised form of the proposed BLE:

$$\text{Non-dimensional Projectile Momentum} = D[1 - e^{-C(1 - \sigma_{hoop}/\sigma_{ult}^{comp})}] \quad (5)$$

in which the term involving the as-yet unknown coefficients B and C , $-Be^C$, has been replaced by a new unknown, D . The remaining unknown coefficients, C and now D , are now chosen using a process that results in the BLE curve falling, to the largest degree possible, between the red and blue data points in Figure 4.

This process begins by selecting an appropriate value of non-dimensional projectile momentum when the non-dimensional hoop stress is ~ 0.000 . If this value is denoted as P_o , then Eqn (5) could be used to solve for D and would then be re-written as follows:

$$\text{Non-dimensional Projectile Momentum} = P_o \frac{1 - e^{-C(1 - \sigma_{hoop}/\sigma_{ult}^{comp})}}{1 - e^{-C}} \quad (6)$$

This now leaves C as the sole remaining parameter to adjust to ensure the proper placement of the BLE curve among the blue and red data points. After several trial runs with a value of P_o chosen so that the BLE intersects the y-axis just below the lowest red data point in Figure 4, it was found that merely adjusting C would not result in a BLE curve that would satisfy condition (1) above. This situation was rectified through the addition of another adjustable parameter, E , into Eqn (6) as follows:

$$\text{Non-dimensional Projectile Momentum} = P_o \frac{1 - e^{-C(1 - \sigma_{hoop}/\sigma_{ult}^{comp})E}}{1 - e^{-C}} \quad (7)$$

After several additional iterations, the values of the constants P_o , C , and E in Eqn (7) that resulted in a BLE having the characteristics noted above as conditions (1) and (2) were found to

be $P_o = 6.0 \times 10^{-3}$, $C = 4.00$, and $E = 7.35$. This completes the derivation of the COPV BLE based on the P/NP data in [8-10].

5.3.2 Comparison with Experimental Results

Figure 5 shows a plot of the proposed BLE amid the data from Figure 4. As can be seen from this figure, the BLE developed in this fashion, with the exception of the single “errant” point noted previously, correctly discriminates between regions of liner perforation and non-perforation.

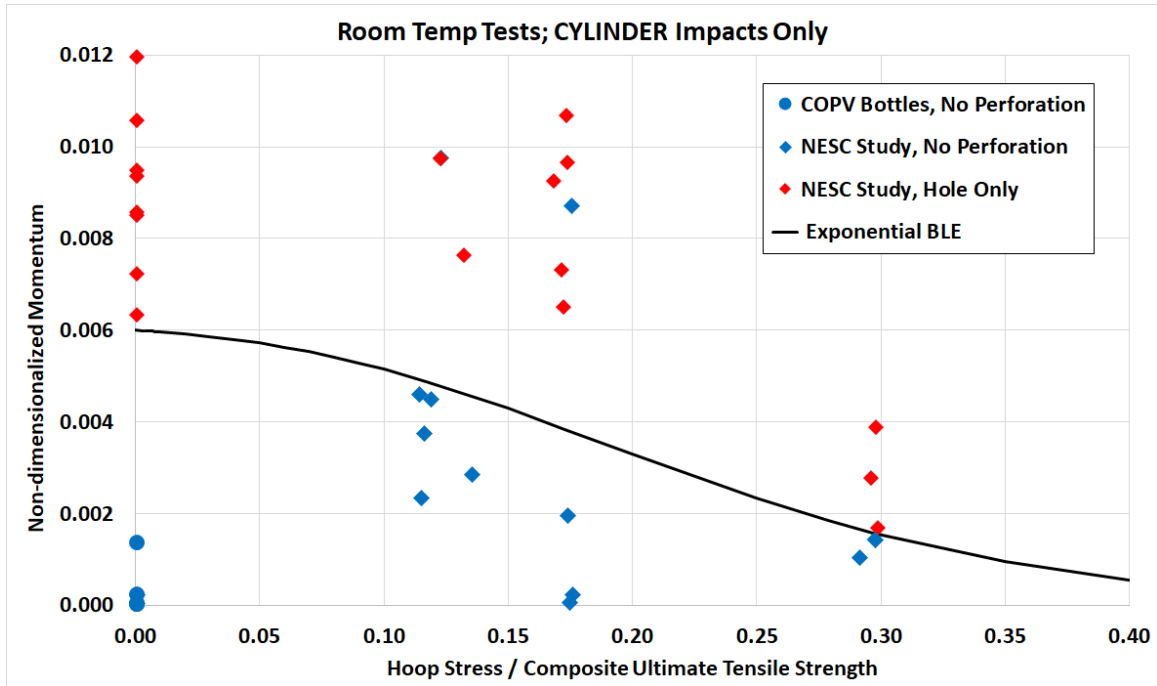


Figure 5. Plot of Exponential BLE Curve and Experimental Perforation/Non-Perforation Data

5.3.3 Comments on the Data

With the exception of a single blue (or non-perforation) data point in Figures 4 and 5, all of the red (perforation or venting, but non-rupture) data points appear to be grouped separately from the blue (non-perforation) tests. With regard to this apparent errant non-perforation data point, Table 3 below presents a summary of the impact test parameters for this test (Test 15) as well as for another test (Test 03) under similar operating conditions (i.e. internal pressure) and with a similar normal component of impact momentum. Figures 6a,b below show the cross sections of the damage areas for these two tests.

Table 3. Summary of Impact Test Parameters

Test No.	Tank Pressure (psi)	Impact Velocity (km/s)	Projectile Diameter (cm)	Impact Obliquity
3	4234	7.41	0.100	47°
15	4270	4.08	0.201	45°

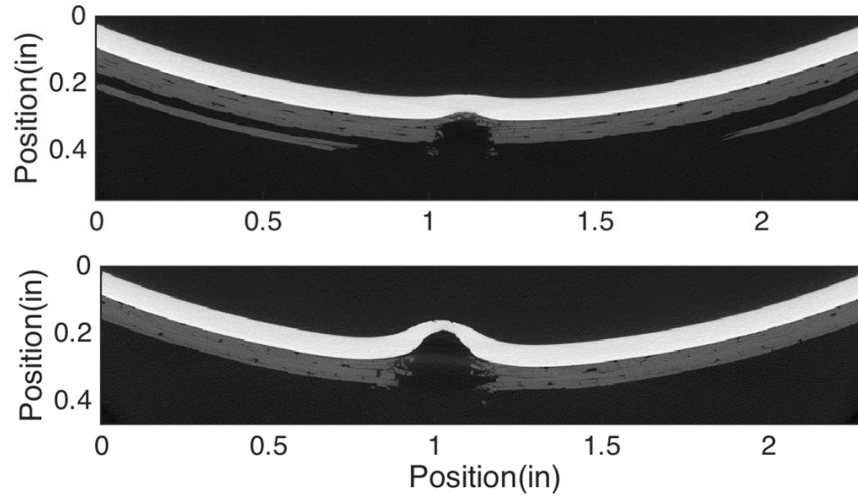


Figure 6a,b. Cross Sections of Impact Damage for Test 03 (top) and Test 15 (bottom)

A quick calculation reveals that the liner thickness at its thinnest point (i.e. the crater floor) in Test 15 was less than 50% of its original thickness, while that of the liner thickness at its thinnest point in Test 03 was just over 80% of its original thickness. Subsequent pressurization of both test articles resulted in a hydro-burst pressure of 4654 psi for Test 15 (that is, only slightly above the internal pressure at testing), while the burst pressure for Test 03 was 6158 psi. Closer inspection of Figure 6b show a number of cracks forming at the floor of the liner crater on the “inside” of the COPV. Based on this information, it would appear that Test 15 was, in fact, likely on the verge of being perforated, but was not, possibly due to some variations in the materials from which that particular test article was made.

5.4 Hole-Out Area Empirical Modelling

The development of empirical equations to predict composite overwrap and liner hole-out areas in the event of liner puncture can be of significant importance in a spacecraft risk assessment calculation. If that assessment is to include a determination of whether a hole created by a perforating impact is large enough so that the thrusting force on the spacecraft created by the exhausting fluid would destabilize the orbit of the spacecraft, then clearly an equation to calculate hole-out area is needed. The question of orbit de-stabilization and global dynamic structural response following an MMOD impact was previously considered in a preliminary study that considered impact conditions that would have resulted in the perforation of one of the Space Station Freedom’s habitable modules [11].

5.4.1 Data Overview

Table 4 presents values of composite overwrap and liner hole-out areas obtained in this study. The composite overwrap hole-out area values were calculated using the expression for the area of either a circle or an ellipse and the maximum and minimum measured composite overwrap hole dimensions. With respect to the liner hole-out area values, those in the “Calculated from Hole Dimensions” were determined again by using the expression for the area of either a circle or an ellipse and measured maximum and minimum liner hole dimensions; those in the “C/T Scan (Direct)” column were obtained directly during a C/T scan imaging process using a Matlab routine to count the number of pixels values that were below a certain threshold value; and

finally, those values in the “C/T Scan (Calculated)” were obtained by measuring the distances between the two opposing edges of the holes in orthogonal C/T scan images, and then using the formula for the area of an ellipse to get the value shown. As can be seen in Table 4, most of the liner hole-out area values are within 10% to 20% of each other, which indicates a good level of consistency in the two techniques used to determine liner hole-out areas.

Table 4. Values of Composite Overwrap and Liner Hole-Out Areas

Test No.	Shot No.	Composite Hole-Out Area Values (in mm ²)	Liner Hole-Out Area Values (in mm ²)		
			Calculated from Hole Dimensions	C/T Scan (Direct)	C/T Scan (Calculated)
HITF-A-#04	16160	22.90	3.6	3.7	4.1
HITF-A-#04a	16274	17.48	6.0	6.5	7.7
HITF-A-#05a	16178	49.31	49.3	54.5	52.2
HITF-A-#06a	16211	39.02	39.0	43.0	40.4
HITF-A-#09	16165	43.01	8.6	7.8	9.4
HITF-A-#09a	16275	19.93	7.5	7.7	8.8
HITF-A-#09b	16331	33.54	33.54	15.8	14.9
HITF-A-#10	16166	33.06	15.2	14.8	15.0
HITF-A-#10a	16328	13.85	13.9	15.7	14.5
HITF-A-#10b	16332	29.22	29.22	15.7	14.3
HITF-A-#11a	16329	24.63	24.6	24.8	21.2
HITF-A-#12a	16393	55.13	55.13	45.5	44.4
HITF-A-#13	16169	46.53	4.0	5.2	4.3
HITF-A-#13a	16394	30.19	30.19	16.9	16.6
HITF-A-#14a	16395	49.02	49.02	32.0	30.6
HITF-A-#16	16504	33.18	12.1	12.0	12.1
HITF-A-#16a	16513	20.95	20.95	11.8	11.4
HITF-A-#17a	16514	25.81	25.81	24.4	27.9
HITF-B-#2	17079	175.39	80.5	70.97	73.55
HITF-B-#6	17287	129.45	52.3	77.42	91.93
HITF-B+ -#10	18432	73.89	3.6	2.60	3.10
HITF-C-#02	19124	9.85	2.26	2.16	2.02
HITF-C-#07	19141	38.29	10.6	9.70	9.18

5.4.2 Equation Development

The composite overwrap and liner hole-out area data in Table 4 was used to develop two empirical equations to predict hole-out area as a function of impact and operating conditions. As before, impact conditions were characterized by non-dimensional momentum as given by Eqn (1), while operating conditions were characterized by non-dimensional hoop stress. Both empirical equations were taken to be in the following format:

$$\frac{10,000A_h}{D_C^2(L_C/t_X)^\alpha} = A(1 + \sigma_{hoop}/\sigma_{ult}^{comp})^B[f(NDMOM)]^C \quad (8)$$

where A_h is the hole out area of either the composite overwrap or the liner, D_c and L_c are the nominal diameter and length, respectively, of the cylindrical portion of the COPV, and $NDMOM$ is the non-dimensional impact momentum as defined by Eqn (1).

The quantities α , t_x and $f(NDMOM)$ for each equation are given in Table 5. The values of the coefficients A , B , and C for each equation were also determined through a regression exercise; they are also given in Table 5, along with the correlation coefficient (R) for each regression.

Table 5. Empirical Liner Hole-out Area Equation Coefficient Values

Parameter	CO Eqn	Liner Eqn
α	1	2
t_x	$t_{comp} + t_{liner}$	t_{liner}
$f(NDMOM)$	$NDMOM$	$\frac{NDMOM}{NDBLE} - 1$
A	20.514	0.136
B	2.382	-9.334
C	0.794	0.614
R	78.2%	92.7%

In Table 5, $NDBLE$ is the ballistic limit value of non-dimensional impact momentum for a specific non-dimensional hoop stress and is given by Eqn (7). We note that the format of the term involving the momentum of the impacting projectile was chosen so that liner hole area would go to zero when the impact momentum equals the ballistic limit momentum value. It was not possible to do this for the composite overwrap hole-out area equation since we do not have an equation that predicts the ballistic limit of the composite overwrap material.

5.4.3 Comparison with Experimental Results

Figures 7 and 8 show plots of the composite overwrap and liner hole-out area equations, respectively, for each of the non-dimensional hoop stress values considered in this study (i.e. 0, ~ 0.127 , ~ 0.173 , and ~ 0.297) amid the corresponding test data. In Figure 8, the occasional vertical “data point clusters” (i.e. two or three closely placed, vertically aligned data points) correspond to the multiple values of liner hole-out area for a single test as noted previously in Table 4, and are not indicative of repeated tests or multiple holes in a single test.

Also shown in Figures 7 and 8, and denoted with three large Xs, are the points along the hole-out area prediction curves for hoop stress values of ~ 0.127 , ~ 0.173 , and ~ 0.297 that correspond to values of non-dimensional impact momentum at which rupture might be expected to occur. These values are obtained from the RLE developed previously for these three operating conditions [2]. The ordinates of these three X data points can then be taken as the maximum non-dimensional composite overwrap and liner hole-out areas that can be expected prior to rupture of the COPV. Subsequently, these maximum liner hole-out area values can then be used to determine a maximum expected thrusting force in the event of a perforating on-orbit MMOD impact. Of further note here is that the hole-out area prediction curves for the unpressurized COPVs have no large X at their ends. This is because rupture of unpressurized COPVs was not observed to occur under any impact conditions.

It is evident in Figures 7 and 8 (and from the correlation coefficient values in Table 6) that both the composite overwrap and the liner empirical hole-out area predictor equations appear to fit the data at each hoop stress level fairly well.

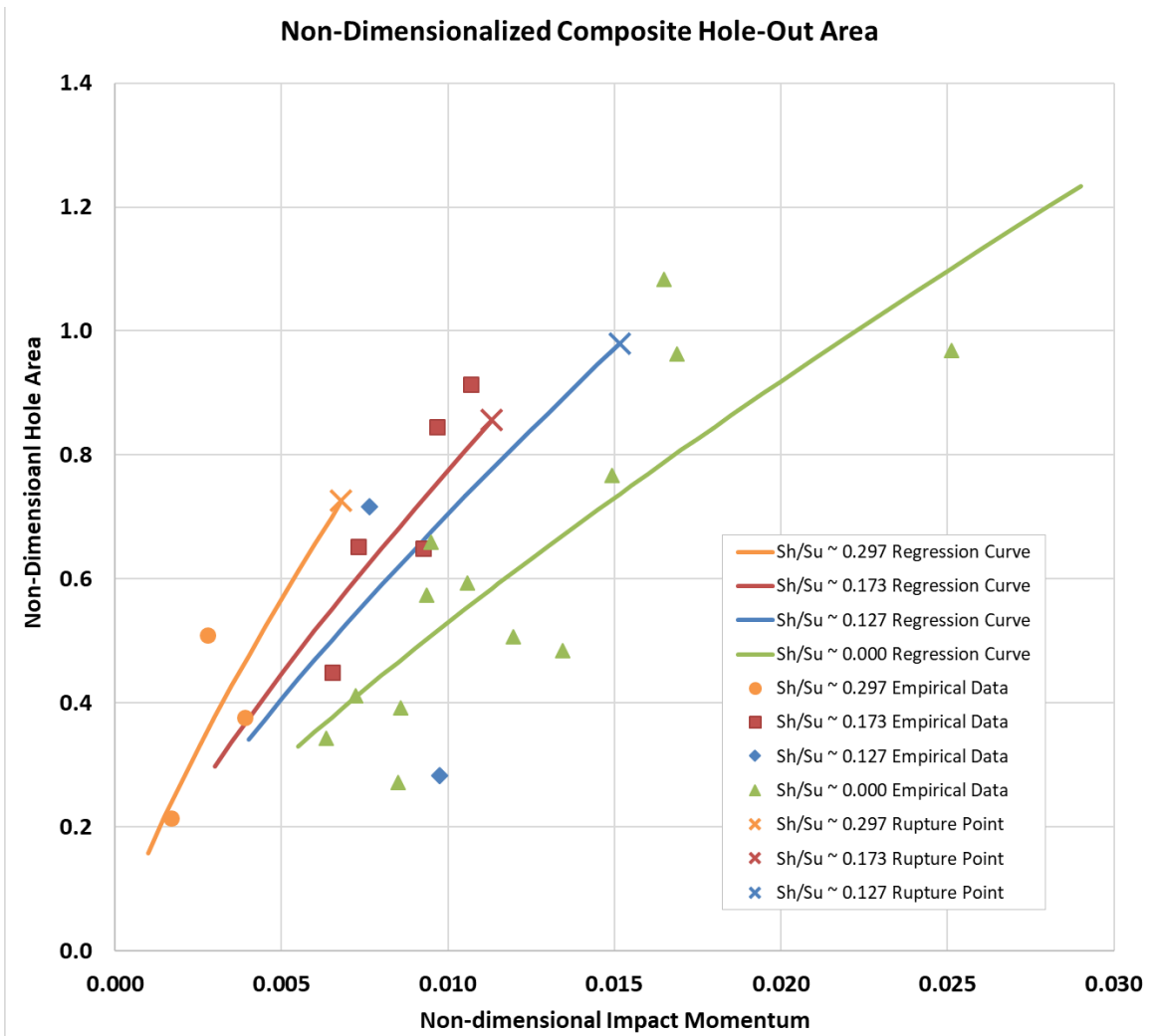


Figure 7. Plot of Empirical Predictions vs. Experimental Data, Composite Overwrap Hole Area

Furthermore, from Figure 7 we see that, for the overwrap material, increasing internal pressure results in increased hole-out area if momentum is kept constant. However, from Figure 8, the opposite trend is evident for liner hole-out area, that is, the size of the hole in the liner appears to decrease with increased internal pressure for constant momentum. Considering the proximity of the liner hole-out area prediction equation curves for the internally pressurized COPVs in Figure 8, it would appear that additional testing might be warranted to confirm this phenomenon.

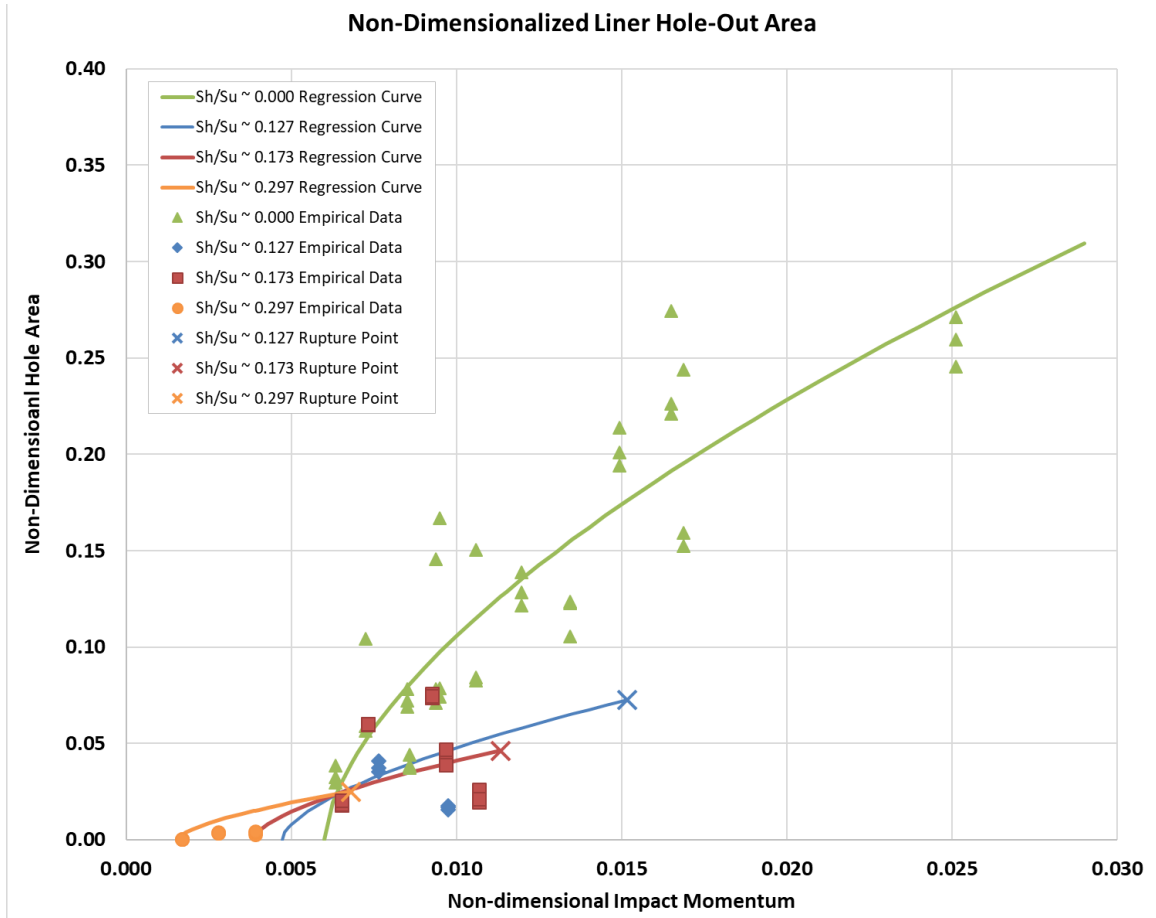


Figure 8. Plot of Empirical Predictions vs. Experimental Data, Liner Hole-out Area

5.5 Concluding Thoughts

This paper presented the results of work performed to address a key issue related to the design of pressurized vessels and tanks that are part of a spacecraft built to operate in the MMOD environment. An empirically-based ballistic limit equation was developed that would differentiate between impact parameters and operating conditions that would result in a hole in an impacted COPV from those that would not. Empirical equations were also developed that can be used to predict hole-out area in the composite overwrap and in the internal liner of a perforated COPV. All the equations developed compared favorably against experimental results. This successfully demonstrates that the approach developed herein can be used to develop this type of equation, and that it can be updated as needed when additional P/NP data becomes available.

An important practical application of the BLE developed herein is predicting whether or not a passivated (i.e. empty or nearly empty) pressure vessel on board an orbiting spacecraft would be perforated following an MMOD impact during the End-of-Life part of its mission. If a BLE developed in this fashion is used for that purpose, the following features of the data and the equations must be noted, understood, and hopefully addressed in the future.

With only a few notable exceptions, the high-speed impact testing performed to date has been done with COPVs made with graphite/epoxy fibers; using spherical aluminum projectiles; with

inert gases or no pressure; at room (or slightly elevated) temperatures; and, using cylindrical-type COPVs. Strictly speaking, the equations developed herein are applicable only to COPVs of similar construction and under similar impact loading conditions. Questions naturally arise regarding how to apply the equations developed herein to, for example, actual orbital environments (e.g. extreme temperature differences; impacts by non-spherical, non-aluminum particles; etc.); to COPVs pressurized with cryogenic fluids, or with propellants or oxidizers; for other COPV shapes, fibers, etc.; and, when the COPV is “shielded” by other spacecraft components. It is hoped that these issues will be addressed in the future.

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5.7 References

1. Schonberg, W.P., and Ratliff, J.M., “Hypervelocity Impact of a Pressurized Vessel: Comparison of Ballistic Limit Equation Predictions with Test Data and Rupture Limit Equation Development”, *Acta Astronautica*, Vol. 115, 2015, pp. 400-406.
2. Schonberg, W.P., “Rupture of a Cryogenic Composite Overwrapped Pressure Vessel Following a High-Speed Particle Impact”, *Aerospace*, Vol. 5, 2018, Paper No. 20.
3. Schäfer, F., Putzar, R., and Lambert, M., “Vulnerability of Satellite Equipment to Hypervelocity Impacts”, Paper No. IAC-08.A6.3.2, Proceedings of the 59th International Astronautical Congress, Sept. 29 - Oct. 3, 2008, Glasgow, Scotland.
4. Schonberg, W.P., Hypervelocity Impact Response of Honeycomb Sandwich Panels, Report No. A-04/08, Fraunhofer Ernst-Mach-Institute, Freiburg, Germany, January 2008.
5. Hereil, P.L., Mespoulet, J., and Plassard, F., “Hypervelocity Impact of Aluminum Projectiles against Pressurized Aluminum-Composite Vessel”, *Procedia Engineering*, Vol. 103, 2015, pp. 181-188.
6. Hassin, J., Pfaab, K., Puillet, C., Limido, J., Lacome, J.L., and Hereil, P., “Hypervelocity Impacts on Composite Overwrapped Pressurized Vessels”, Proceedings of DYNCOMP 2015, Arles, France, June 2015.
7. Schonberg, W.P., “Catastrophic Failure (Rupture) of Composite Overwrapped Pressure Vessels Following Hypervelocity Impact”, *Journal of Spacecraft and Rockets*, submitted for publication, February 2020.
8. M.A.Garcia, B.A.Davis, and J.E.Miller, “Hypervelocity Impact Testing of a Pressurized Composite Overwrapped Pressure Vessel and Comparison to Numerical Analysis”, *Procedia Engineering*, Vol. 204, 2017, pp. 476-483.
9. Davis, B.A., Christiansen, E.L., Miller, J.E., and Yoder, T.B., “Hypervelocity Impact of Composite Overwrap Pressure Vessels”, Paper No. 6102, Proceedings of the 1st International Orbital Debris Conference, Sugarland, Texas, December 2019.

10. Nahra, H., Nichols, C., Christiansen, E., Greene, N., Miller, J., Davis, B., Composite Over-wrapped Pressure Vessel (COPV) Hypervelocity Impact (HVI) Study: Phase – 1 (Impacts on Unpressurized COPV Tank), Johnson Space Center, Houston, TX, 2015.
11. Celestian, J.P., and Schonberg, W.P., “Dynamic Response of the Space Station Freedom to a Module Perforation by a Hypervelocity Impact”, *International Journal of Impact Engineering*, Vol. 13, No. 2, 1993, pp. 353-365.

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