

Article

Tensegrity Launch Tubes and Compliant Mechanisms for Mechanical Versatility in Small-Scale Industrial Line Launchers: A Reliability-Centered Screening Analysis

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Abstract

This study investigated the integration of 3D-printed tensegrity launch tubes and compliant mechanism components into small-scale industrial line launcher systems. A multi-variable experimental design ($n = 108$ replicate shots) evaluated a seven-segment PLA tensegrity tube, the potential for a monolithic compliant launcher, and a pneumatic benchmark across various projectile types, tube configurations, and muzzle rifling geometries. The system exhibited a severe 57.4% launch failure rate, with failures concentrated in extended tube configurations and Rigid or Compliant muzzle attachments. To isolate the dominant operational drivers across the dataset, a composite velocity score was analyzed. Non-parametric variance testing identified tube configuration as the primary factor influencing velocity (Kruskal–Wallis $H = 26.73$, $p < 0.001$), followed by muzzle geometry ($H = 10.20$, $p = 0.017$). Post-test disassembly identified three distinct failure modes, primarily driven by bore clearance rather than the vibrational compliance of the tensegrity architecture. A Failure Mode and Effects Analysis (FMEA) quantified these risks, identifying tensegrity tube bore constriction as the primary threat to system reliability (Criticality = 336). Process capability analysis against Stage 1 prototyping gate criteria confirmed the system is not yet process-capable. Furthermore, a Total Cost of Ownership (TCO) analysis yielded an estimated US \$8.07–11.22 per successful launch, challenging the economic scalability of low-cost additive manufacturing materials. This study establishes quantitative benchmarking and a reliability-centered Design for Additive Manufacturing (DfAM) framework required before scaling toward maritime, emergency, or aerospace applications.

Keywords: tensegrity; compliant mechanism; additive manufacturing; line launcher; reliability engineering; Design for Additive Manufacturing



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1. Introduction

1.1. Overview

Conventional industrial line launcher systems—such as maritime rescue line throwers, emergency tether launchers, and aerospace payload delivery mechanisms—traditionally rely on multi-part assemblies fabricated via subtractive manufacturing. While functional [1,2], these designs are constrained by high assembly labor costs, poor stowage efficiency due to rigid geometries, and reduced operational reliability caused by kinematic joint wear and tolerance stack-up. Additive manufacturing (AM) addresses these limitations by enabling the consolidated fabrication of complex, monolithic geometries that are cost-prohibitive under traditional production paradigms [3,4].

Integrating tensegrity metamaterials with compliant mechanisms further minimizes bill-of-materials (BOM) complexity and eliminates assembly bottlenecks. Tensegrity architectures suspend discontinuous compression elements within a continuous tension network, offering high strength-to-weight ratios, tunable energy absorption, and high volumetric deployment ratios [5,6]. Recent advances in multi-material three-dimensional (3D) printing allow the automated, monolithic fabrication of centimeter-scale tensegrity systems previously constrained by high-variance manual assembly [4,6–25]. Complementarily, compliant mechanisms achieve motion and energy storage through elastic deformation rather than traditional kinematic pairs, eliminating friction, backlash, and joint wear [3,12]. Prior work demonstrated the efficacy of this approach by consolidating an 80-part commercial launcher into a single-piece 3D-printed compliant mechanism with broad functional scalability [3]. When produced via AM, compliant geometries offer significant potential to enhance projectile stability and launch capabilities in line launcher systems.

Project QINGLONG investigates the systematic integration of these additive manufacturing paradigms within small-scale industrial line launcher systems. This study presents a Stage 1 proof-of-concept rapid prototyping evaluation at the micro-launcher scale (projectile mass < 4 g, exit velocity < 40 m/s) utilizing commodity fused deposition modeling (FDM) hardware and polylactic acid (PLA) filament. Leveraging accessible, low-cost hardware serves as a key methodological strategy: it allows Design for Additive Manufacturing (DfAM) reliability benchmarking at minimal capital cost prior to committing to engineering-grade materials or industrial-scale fabrication [6–25]. Long-term industrial applications target maritime rescue operations aboard Safety of Life at Sea (SOLAS)-regulated vessels [16], emergency vertical access in confined spaces, and aerospace microsatellite operations requiring minimal stowage volume and negligible recoil [9–11].

To evaluate system performance, this study systematically investigates how launch tube configuration (absent, fully extended, or angled), projectile type (Foam darts, rigid PLA cylinders, or composite dart-dowel assemblies), and muzzle rifling geometry (absent, soft Nylon, Rigid plastic, or Compliant printed) influence exit velocity, as measured via ballistic chronography. A $3 \times 3 \times 4$ full factorial design yielding 36 unique treatment combinations is implemented to establish quantitative prototyping benchmarks, failure-mode taxonomies, and DfAM design rules. Given the small per-cell replication inherent to commodity hardware testing, inferential conclusions are restricted to main effects and large pairwise contrasts, while higher-order interaction claims remain exploratory.

The remainder of this manuscript is organized as follows: Section 1.2 presents the theoretical and technical background. Section 2 details the experimental methodology, covering materials, fabrication, test protocols, and statistical frameworks—including Failure Mode and Effects Analysis (FMEA), process capability, and Total Cost of Ownership (TCO) modeling. Section 3 reports empirical results, including regression analysis, composite scores, and conditional velocity testing. Section 4 provides an interpretation of the findings, an industrial scaling roadmap, study limitations, and directions for future research. Finally, Section 5 summarizes the core conclusions of this work.

1.2. Background

1.2.1. Industrial Line Throwers

Industrial line throwers launch a projectile with a trailing rope, often by pneumatic means. Pneumatic launchers convert pressurized gas potential energy into kinetic energy via a piston-cylinder mechanism. In modern UAV launch systems, the aircraft fuselage can act as the piston, sliding over a stationary launch tube cylinder. Energy-conservation models predict launch velocities within 10% of IMU-verified experimental data [18]. Similarly, pneumatic line throwers (LTAs) are established systems for maritime and emergency line

deployment. These LTAs utilize accumulated pressure to propel a weighted projectile and line without reverse blast. Commercial systems typically operate at 200–300 bar to deliver multiple maximum-pressure launches per charge [1,2,9]. Beyond maritime rescue, this technology is also utilized for forestry pruning and construction cable deployment [1,2,9]. In these roles, weight is often an issue, making the concept of tensegrity-based launchers attractive.

1.2.2. Tensegrity Masts

Tensegrity systems employ an internal equilibrium of discontinuous compression struts and continuous tension tendons. As previously reported, this configuration yields high deployability—demonstrated by historical space-deployable masts achieving stowage ratios down to 4.8% of deployed length [6]. For industrial line launchers operating under strict volumetric and mass constraints (e.g., maritime rescue or aerospace payloads), this minimizes transit volume while enabling rapid, on-demand erection.

Historically, manual fabrication—requiring precise geometric calibration, prestressing, and knotting—created an industrial bottleneck that compromised scalability and repeatability [6,7]. Recent AM advancements claim to mitigate this by consolidating discrete components into a unified build, reducing bill-of-materials (BOM) complexity and assembly labor. Viable automation pathways include Dual-Nozzle AM with Sacrificial Molding and Sequential Multi-Step Molding. Dual-Nozzle AM with Sacrificial Molding integrates smart material tendons with rigid struts post-mold dissolution [4]. Sequential Multi-Step Molding utilizes shape-memory polymer (SMP) bars and silicone tendons to fabricate centimeter-scale tensegrity metamaterials [8].

For launch tube applications, the metamaterial behavior of tensegrity lattices is highly advantageous. Optimization models of truncated octahedral tensegrity units show that energy absorption scales cubically with unit size and prestress level, leveraging controlled strut buckling under forced displacement [5]. There are several types of tensegrity masts.

1.2.3. Tensegrity Structure Types

The three reported primary approaches for tensegrity masts are summarized in Table 1. Monolithic AM metamaterial masts represent the first approach; while they require few parts and eliminate assembly labor, they often fail due to bore constriction. The second approach, cable-actuated clustered booms, relies on sensors and motors to actively maintain loads [5–7]. Without real-time feedback and active load control, these systems are susceptible to rapid, non-linear failure. The third approach, spring-driven kinematic mechanisms, combines meshed gears and hybrid structures. However, these mechanical components are prone to wear down over time, decreasing long-term reliability [5–7].

Table 1. Tensegrity mast launch tubes.

Feature	Monolithic AM Metamaterial Masts	Cable-Actuated Clustered Booms	Spring-Driven Kinematic Mechanisms
Primary Actuation	Elastic strain energy release/Compliant mechanisms	Motorized winches and active cable-length tuning	Strain energy release from discrete compression/tension springs
Mechanical Advantage	Zero assembly labor, minimal parts, excellent recoil dampening	Highly scalable, high stowage efficiency	Deterministic path, exceptional structural stability
Primary Failure Mode	Bore constriction under load, material fatigue, high launch failure rates	Non-linear geometric deformation, high friction, requires active control	Joint wear, increased mass from rigid gears/linkages, constraint jamming
Kinematic Predictability	Highly non-linear	Non-linear	Highly deterministic

1.2.4. Compliant Mechanism Integration

A potential benefit to AM in tensegrity mast construction is the use of compliant geometries. Unlike traditional rigid-body systems, compliant mechanisms achieve motion through the elastic deformation of flexible members. This monolithic design eliminates kinematic joints, which reduces overall part count, eliminates backlash, and facilitates AM [3,15]. For energy-storage applications, constant-force mechanisms (CFMs) are reported to be particularly advantageous. By maintaining a nearly uniform output force across their displacement range, CFMs maximize the area under the force-displacement curve, thereby optimizing elastic strain energy storage [12]. Furthermore, in catapult applications, CFMs enhance launch dynamics by outperforming traditional linear springs; they release energy more efficiently, yielding higher launch velocities for the same amount of input work [17].

As reported in prior work, bistable compliant mechanisms store energy by crossing an energy barrier between two stable equilibrium states, rapidly releasing that energy during snap-through [13,14]. Recent work highlights the scalability and manufacturing advantages of these monolithic designs, particularly in system consolidation and multi-scale viability. For instance, one prominent study consolidated an 80-part commercial toy launcher into a single, 3D-printed compliant mechanism that stores energy in bent beams and utilizes an integrated trigger flexure [3]. Leveraging the ability of additive manufacturing to scale geometry and material layouts without retooling, this monolithic launcher reportedly maintained functionality across scale factors ranging from $0.01\times$ to $7.25\times$ [3]. To design these systems, standardized synthesis methods for compliant constant-force and constant-torque devices generally leverage AM-compatible frameworks: kinematic limb-singularity, beam buckling, and topology optimization [15]. These benefits could potentially be applied to projectile launch and aerodynamic stabilization.

1.2.5. Compliant Projectile Stabilization and Rifling Concepts

Aerodynamic stability dictates projectile accuracy and effective range, and thus the efficiency of line launchers. While conventional firearms utilize helical rifling to impart gyroscopic spin, rigid rifling causes excessive friction, deformation, and velocity loss in compliant projectiles like Foam darts. Historically, line launchers such as the Schermuly pistol-rocket and Lyle cannon bypassed spin-stabilization, relying instead on mass distribution and aerodynamic geometry [1,2]. Recent research investigated “soft rifling” configurations—originally developed by Foam-dart hobbyists to minimize dispersion [19,20]—which utilize tensioned, angled textile threads to guide the projectile while compressing to reduce frictional drag [9]. While line launchers and compliant mechanisms are all separate, mature technologies, the use of compliant mechanisms as “rifling” in a line launcher has not been explored in prior literature.

1.2.6. Research Gap and Objectives

While tensegrity structures, compliant mechanisms, and line launchers were individually mature, the peer-reviewed literature lacked systematic integration of these technologies into a unified, small-scale industrial line launcher framework. Likewise, compliant rifling has not been explored in projectile launchers previously. Specifically, the coupled physical interactions among deployable tensegrity launch tubes, the potential for monolithic compliant launchers and compliant rifling, and variable rifling geometries remained unquantified regarding exit velocity, consistency, and reliability. Project QINGLONG addressed this gap through four primary objectives.

Project QINGLONG designed, FDM 3D-printed, and evaluated a seven-segment tensegrity metamaterial-based launch tube across three geometric configurations, utilizing an identical acrylic Rigid Control tube to isolate geometric impedance from tensegrity

compliance effects. Second, it benchmarked a monolithic compliant mechanism launcher against a pneumatic baseline across multiple projectile types to validate DfAM integration. Third, it quantified how a smoothbore muzzle, soft Nylon, rigid plastic, and Compliant printed rifling affected launch consistency, velocity, and failure rates. Finally, the project demonstrated that a reliability-centered DfAM approach using commodity PLA established quantitative benchmarks for small-scale deployment, identifying the performance thresholds required prior to scaling for maritime, rescue, or aerospace operations.

2. Materials and Methods

2.1. Design for Additive Manufacturing (DfAM) Process and Material Selection

As shown in Figure 1, the tensegrity launch tube consists of seven discrete polylactic acid (PLA) segments fabricated via FDM (Bambu Lab A1 Mini from Shenzhen, Guangdong, China, 0.4 mm nozzle, 0.2 mm layer height, 40% infill). Each segment forms a truncated triangular unit incorporating interlocking conical beads (3 mm ID, 16 mm OD) for tension-member anchoring and multi-segment articulation [5–8,21].

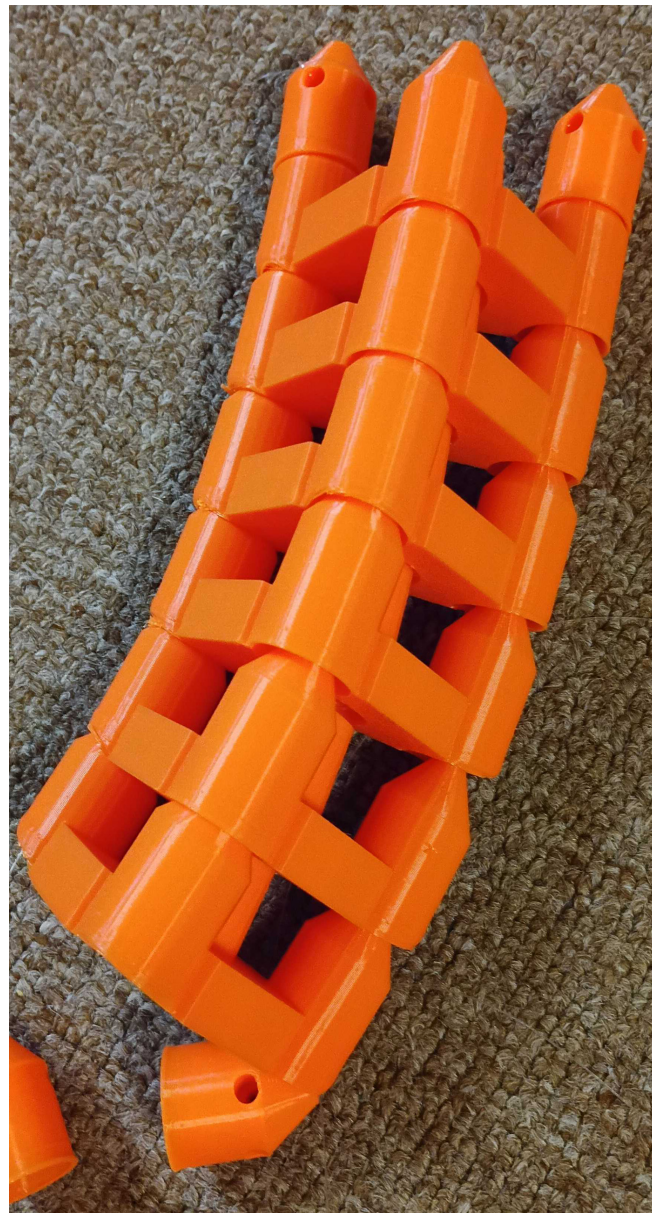


Figure 1. Tensegrity launch track being assembled.

PLA (Hatchbox 3D, Ponomo, CA, USA) was selected as the material due to an Ashby-style analysis of specific stiffness (E/ρ) and glass transition temperature (T_g) relative to the operational envelope. PLA ($E \sim 3.5$ GPa, density ~ 1.25 g/cm³, $T_g \sim 60$ deg C) was chosen over PETG and ABS for Stage 1 due to its superior stiffness-to-density ratio and low creep susceptibility under room-temperature prestress. Although limited by brittleness and moisture sensitivity, PLA prioritizes accessibility and printability over ruggedness, deliberately bounding the operational scope [26].

2.2. Tensegrity Launch Tube Fabrication and Modes

As shown in Figure 1, the tensegrity launch tube comprises seven discrete PLA segments fabricated via FDM using a Bambu Labs A1 Mini (0.4 mm nozzle, 0.2 mm layer height, 40% infill for load-bearing struts). Each segment is a truncated triangular unit inspired by prior optimization frameworks [5–8,21]. To facilitate tension-member anchoring and multi-segment articulation, the segments feature interlocking conical beads with 3 mm inner and 16 mm outer diameters.

The segments are bound with 0.4 mm-diameter Nylon monofilament (KastKing, Garden City, NY, USA) threaded through pre-modeled node channels, tensioned via a friction-knot protocol, and fixed in place. At full extension, the resulting tube is 160 mm long with a 16 mm internal diameter, providing a 1.65 mm radial clearance for the test projectiles. Fabrication of the seven-segment tube took 18 min.

The tube operates in two primary states. In the stowed state, the compliance of the tension network allows the seven segments to collapse into a compact, zigzag configuration, achieving a high stowage ratio [6]. The 6 mm-diameter node spheres at each strut intersection serve as both compression load-transfer points and tension-network anchors. Upon tensioning to approximately 5 N per segment (measured using a digital scale with 0.1 N resolution), the segments align coaxially into a continuous 160 mm barrel. The high-contrast orange PLA struts against the reflective monofilament allow rapid visual verification of alignment and tension integrity prior to testing.

In the Full Extension configuration, the tube was prestressed to approximately 5 N per segment. This preload induced a bound state where segmental nodes were locked against translational movement by the tension network. Static axial compression testing of the assembly approximated an effective axial stiffness matching a solid PLA cylinder of equivalent outer diameter, validating that the bound state remains rigid under static loading. Visual monitoring showed negligible segmental buckling during projectile passage. However, classifying the tube as a rigid extended barrel is qualified by the lack of high-speed videography (≥ 1000 fps) to capture transient dynamic compliance during the pneumatic impulse (estimated duration < 10 ms). The Full configuration thus serves as a control isolating the effects of barrel extension (160 mm) and internal bore geometry (16 mm diameter, 1.65 mm radial clearance) from the tensegrity architecture's compliance, though transient vibrational effects cannot be definitively excluded. The Full Angled configuration (15 degrees off-axis) shares the same segment geometry and tensioning protocol to isolate geometric non-concentricity. Prestress was verified before each nine-shot experimental block; segments with $>10\%$ prestress decay were retensioned. No statistically significant prestress drift occurred across the 12 blocks (mean decay = 3.2%, SD = 4.1%), ruling out temporal relaxation as a confound.

To normalize for hygroscopic effects on mass and friction coefficients, all PLA components were stored in a sealed container with desiccant for 24 h prior to testing. Gravimetric moisture analysis of seven representative segments before and after conditioning confirmed a reduction in mean moisture content from 0.38% ($\pm 0.07\%$) to 0.12% ($\pm 0.03\%$).

Three tube configurations were evaluated: (i) Rigid Control, an acrylic tube (160 mm length, 16 mm internal diameter) positioned over the chronograph to serve as a non-tensegrity baseline that isolates barrel-length and impedance effects; (ii) Full Extension, where the tensegrity tube was fully elongated and secured coaxially with the launcher barrel via a printed bayonet mount; and (iii) Full Angled, where the tensegrity tube was extended and secured 15 degrees off-axis via a Compliant printed gimbal joint to simulate industrial inspection around obstacles. While the acrylic control maintains geometric equivalence to the tensegrity extension, its material properties (thermal expansion, surface hardness, and friction coefficient) differ from PLA.

2.3. Launcher Systems

The compliant launcher data are retained for comparative discussion but excluded from the primary statistical models due to confounding with other experimental factors. The launcher was fabricated as a monolithic, single-piece PLA compliant mechanism that stores strain energy via bent flexible beams and uses a latching flexure trigger [3]. Scaled to fit the test projectiles, the component was printed with a 0.2 mm layer height and 40% infill within the flexure regions. Key design parameters include an overall launcher length of 160 mm and a central pull-bar draw length of 30 mm. Beam thickness was engineered to produce a 15 N draw force, optimizing energy storage for projectile acceleration while ensuring single-handed operation. Figure 2 illustrates the assembled launcher with a nonessential support casing added for handling and test fixture mounting.

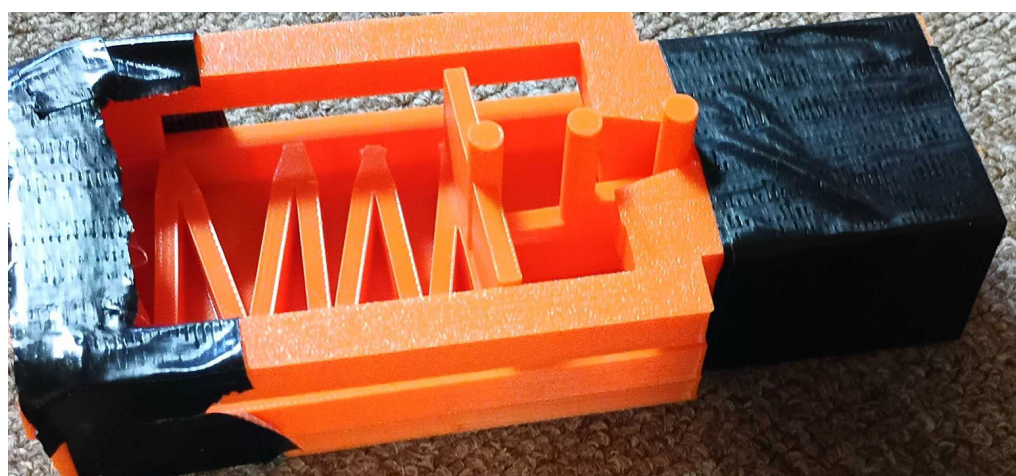


Figure 2. Compliant launcher prototype with non-essential support and test fixation frame.

Screening trials using a test Foam projectile (mass mean = 1.0 g, SD = 0.03 g) yielded exit velocities of 4.9, 6.7, and 4.9 m/s (mean = 5.50 m/s, SD = 1.04 m/s). Assuming linear elastic behavior for the flexure array, the effective spring constant was estimated via Hooke's law by equating stored elastic potential energy to the projectile's kinetic energy in Equation (1) [10–13]:

$$1/2 kx^2 = 1/2 mv^2 \quad (1)$$

where $m = 0.001$ kg, $x = 0.03$ m, and v is the observed exit velocity. Substituting the mean velocity yielded an effective $k \sim 34$ N m⁻¹, with a shot-to-shot range of 27 N m⁻¹ (4.9 m s⁻¹) to 50 N m⁻¹ (6.7 m s⁻¹). The total launcher mass was 105 g, rendering recoil negligible and validating the energy-balance assumption. The corresponding stored elastic energy ranged from approximately 0.012 to 0.023 J per shot, confirming that the compliant launcher occupies a low-energy regime distinct from the pneumatic benchmark.

The compliant mechanism launcher was evaluated against a high-power pneumatic benchmark—a commercial Dart Zone Outlaw blaster (Prime Time Toys, Pompton Lakes, NJ, USA). Operating via a spring-driven piston compressing air within a sealed cylinder, the Outlaw delivers a rapid pressure pulse to the projectile. Screening trials using ~1.0 g projectiles yielded exit velocities of 25, 24, and 35 m/s (mean = 28.0 m/s, SD = 6.08 m/s).

Preliminary screening confirmed that the pneumatic system provided higher mean velocities and lower coefficients of variation across all projectile geometries. Consequently, it was selected as the primary test platform for extended tube-and-rifling factorial experiments. Data from the compliant launcher are retained for comparative discussion but excluded from primary statistical models to avoid confounding the launcher mechanism with other experimental factors.

2.4. Projectile Designs

Three projectile types representing small-scale deployment payloads were evaluated: (1) Foam darts (Dart Zone Max from Prime Time Toys in Pompton Lakes, NJ, USA; 12.7 mm diameter, 38 mm length; $m \approx 1.0$ g, $SD = 0.03$ g), modeling lightweight, compressible payloads like messenger lines or soft sensors; (2) Printed cylinders (rigid PLA; 12.7 mm $\times$ 38 mm; $m \approx 3.0$ g, $SD = 0.2$ g), modeling high-stiffness payloads like rigid NDE sensors or probes; and (3) Combined Darts (Foam darts with a 3 mm diameter, 30 mm long central PLA dowel; $m \approx 1.3$ g, $SD = 0.1$ g), modeling hybrid payloads requiring core rigidity with outer compressibility for barrel engagement. To eliminate mass and friction variations caused by PLA hygroscopicity, all projectiles were pre-conditioned in a desiccant-sealed container for 24 h (Figure 3).

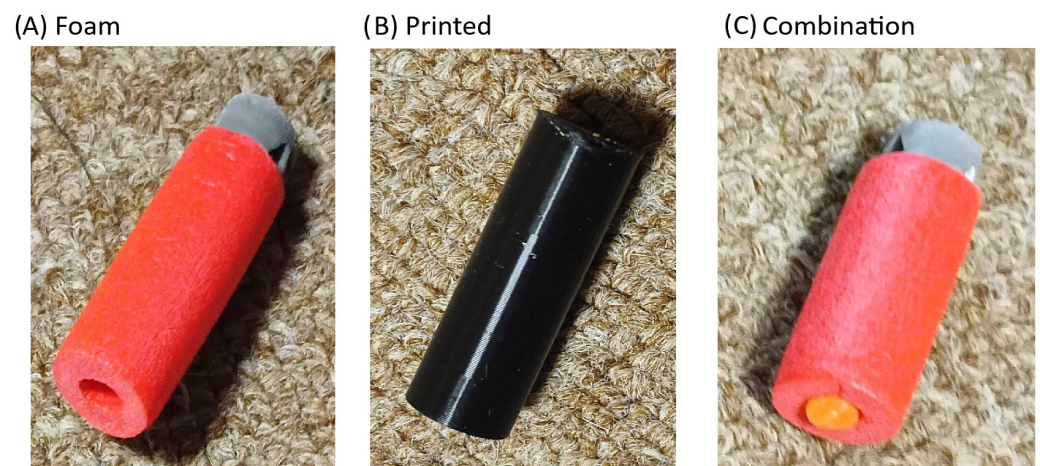


Figure 3. Three dart types presented: (A) Foam; (B) 3D-printed; (C) combination of Foam and 3D-printed PLA.

2.5. Muzzle Rifling Configurations

Four muzzle configurations were evaluated: (i) a smoothbore Control establishing a velocity baseline; (ii) Nylon soft rifling, utilizing four pairs of holes angled at 15 degrees and threaded with 2 N tensioned Nylon line to minimize friction; (iii) Rigid plastic rifling, featuring four helical lands (0.5 mm height, 1:20 twist rate) to engrave projectiles; and (iv) Compliant printed rifling, employing four helical flexures (0.8 mm thickness, 1:20 twist rate) with living-hinge bases for elastic deflection and low-friction spin. All 25 mm attachments were friction-fitted to the barrel and 3D-printed in PLA using a 30% infill and a 0.2 mm manufacturing resolution and layer height.

2.6. Experimental Environment and Design

Muzzle velocity was measured 0.5 m downstream from the launcher exit using a pre-calibrated ballistic chronograph (Tianjin Watchmaking Group, Tianjin, China) (m/s). Chronograph placement minimized parallax error. Measurement systems analysis (MSA) yielded a Gage R&R precision-to-tolerance ratio of 8.4% across the 0–40 m/s range and a discrimination ratio of 5, confirming adequate precision. Calibration traceability was verified against a reference standard (expanded uncertainty $U = 0.3$ m/s, $k = 2$). The sensor detection threshold was 1.2 m/s; lower values were classified as zero-velocity launch failures. Post-test inspections verified mechanical obstructions in all failure cases, resolving classification ambiguity for sub-threshold velocities. Tests were conducted in a climate-controlled room (20 ± 2 deg C, $45\% \pm 10\%$ RH) to stabilize PLA material properties and dart compliance. A rigid test fixture eliminated operator variance from aiming and trigger actuation.

The experiment evaluated a full factorial design ($3 \times 3 \times 4$) with 3 replicates per treatment, totaling 108 trials across all 36 combinations. Independent variables were projectile type (Foam, Printed, Combined), tube configuration (Rigid Control, Full Tensegrity, Full Angled Tensegrity), and muzzle rifling (Standard, Nylon, Rigid, Compliant). To mitigate temporal drift and thermal accumulation, runs were randomized within 9-shot blocks (3 projectiles $\times$ 3 muzzles per tube configuration) separated by 5 min cooling intervals. Blocks were stratified by tube configuration over consecutive testing days, nesting temporal variance within tube levels.

A priori power analysis for binomial proportions indicated that $n = 3$ per cell yields ~80% power to detect a 35 percentage-point difference in failure rates ($\alpha = 0.05$). The sample size is adequate for identifying dominant main effects and select pairwise contrasts but underpowered for subtle main effects or multi-way interactions. Interaction effects are therefore reported as exploratory hypotheses requiring further validation.

2.7. TCO Framework

For TCO calculation, equipment costs were determined by evaluating 47.2 printer-hours at an opportunity cost of \$5.00 to \$8.00 per hour, accounting for laboratory depreciation, maintenance, and utility allocation. Labor costs were derived from 4.5 h of technical staff effort at \$25.00 per hour, covering calibration, tensegrity assembly, tensioning, pre-stress verification, post-test inspection, and re-tensioning [26]. The system failure rate FR is required to calculate it, as detailed in Equation (2).

$$\text{Scrap Multiplier} = 1/(1 - \text{FR}) \quad (2)$$

2.8. Failure Analysis Framework

FMEA was conducted per IEC 60812 guidelines [27]. Two independent raters evaluated Severity (S), Occurrence (O), and Detection (D) using standard 1 to 10 rating scales (Cohen's kappa = 0.84). Occurrence ratings were derived from empirical failure rates across $n = 108$ test shots using standard industrial probability bands. Detection ratings were assigned based on identification ease and timing. Criticality was calculated using the Risk Priority Number formula: $\text{CRIT} = S \times O \times D$. Economic impact was quantified using a Taguchi quadratic loss function, $L(y) = k \times (y - 15)^2$, relative to the 15 m/s Stage 1 target. Treatment cell distribution was evaluated to determine appropriate statistical modeling.

2.9. Statistical Analysis

Data analysis was conducted using Python 3.11 with the statsmodels package. Although a three-way mixed-model ANOVA with nested random blocking was originally planned,

severe cell imbalance from a high failure rate could render the model under-identified. Primary inference therefore relied on non-parametric and robust statistical methods.

Failure probabilities were evaluated using logistic regression with a logit link and maximum likelihood estimation, using likelihood ratio chi-squared tests to assess goodness of fit. Differences in failure rates across factor levels were tested using chi-squared tests of independence.

For continuous velocity metrics—composite ($n = 108$) and exploratory conditional ($n = 46$)—omnibus comparisons were performed using Kruskal–Wallis H-tests, followed by post hoc Mann–Whitney U tests with Bonferroni corrections for pairwise contrasts. Pairwise effect sizes were reported as Cohen’s d computed from pooled standard deviations. Interaction effects were probed via stratified two-way Kruskal–Wallis tests within each tube configuration. Parametric models used for sensitivity analysis were evaluated for assumptions using Levene’s test for homoscedasticity and Shapiro–Wilk tests for normality.

Process capability indices (C_p , C_{pk}) were calculated for the Rigid Control smoothbore configuration against Stage 1 gate criteria. While C_p / C_{pk} indices traditionally assume continuous, normally distributed data, they were calculated for binary failure data to provide a standardized, comparable metric across all process variables. By aggregating binary pass/fail outcomes into rates per subgroup, the Central Limit Theorem allows for a normal approximation. Alternatively, the reported values represent an equivalent C_{pk} derived via Z-score conversion ($C_{pk} \approx Z/3$) to align with enterprise capability reporting requirements. All analysis scripts, raw data, and computational models are included in the supplementary materials [21–28].

3. Results

3.1. Organizational Summary

Before the primary batch of trials, the comparison between the compliant launcher and pneumatic launcher was conducted. After trials concluded, TCO and FMEA were calculated. Subsequent analysis centered on the experimental results ($n = 108$).

3.2. Compliant Mechanism Versus Pneumatic Benchmark

To quantify performance boundaries, the monolithic compliant launcher was directly compared against the pneumatic benchmark (Dart Zone Outlaw) using Foam-dart projectiles ($n = 3$). The compliant launcher achieved a mean velocity of 5.50 m/s ($SD = 1.04$, $SEM = 0.60$), whereas the pneumatic benchmark reached 28.00 m/s ($SD = 6.08$, $SEM = 3.51$). A Welch’s t -test confirmed a statistically significant separation ($t(2.12) = -6.32$, $p = 0.021$, Cohen’s $d = 5.16$). Given the sample size, these metrics serve as a descriptive benchmark rather than confirmatory population inferences, particularly given the high sample variance (notably 6.7 m/s and 35 m/s maximums, respectively) and constrained degrees of freedom ($df \sim 2.12$).

Because kinetic energy scales quadratically (E_k proportional to v^2), the benchmark’s 5.2-fold velocity advantage yields an approximate 26-fold increase in kinetic energy, demonstrating non-overlapping operating regimes. This energy disparity justified excluding the compliant launcher from the primary tube-and-rifling factorial design, as treating launcher type as a crossed factor would introduce severe output confounding and obscure geometric effects. While useful for DfAM workflow validation, the compliant launcher failed the Stage 1 process capability gate (mean velocity ≥ 15 m/s).

3.3. FMEA Result

The Taguchi loss for total launch failure ($y = 0$ m/s) was 225 k compared to 0.92 k at the Rigid Control mean ($y = 15.96$ m/s). This 244:1 loss ratio confirms that design optimization

must prioritize reliability over minor velocity variations. Finally, multiple treatment cells contained fewer than three successful replicates, and two yielded zero successful launches; this severe sparsity validated the use of non-parametric statistical methods over classical mixed-model ANOVA.

3.4. Expanded Launch Failure Analysis

As shown in Figures 4 and 5, experimental testing revealed a high rate of launch failures. Across 108 trials, 62 shots (57.4%) produced zero exit velocity, resulting from projectile jamming within the barrel assembly or exit failure. Failure rates varied significantly by tube geometry: Rigid Control tubes exhibited a 33.3% failure rate (12/36), whereas both Full and Full Angled configurations experienced a 69.4% failure rate (25/36 each). This variation across configurations was statistically significant (chi-square = 12.80, $df = 2$, $p = 0.002$). Other figures, such as the Pareto chart of failure modes, the Design Evolution Roadmap, CAD diagrams, and other files, are in the Supplemental Files [28].

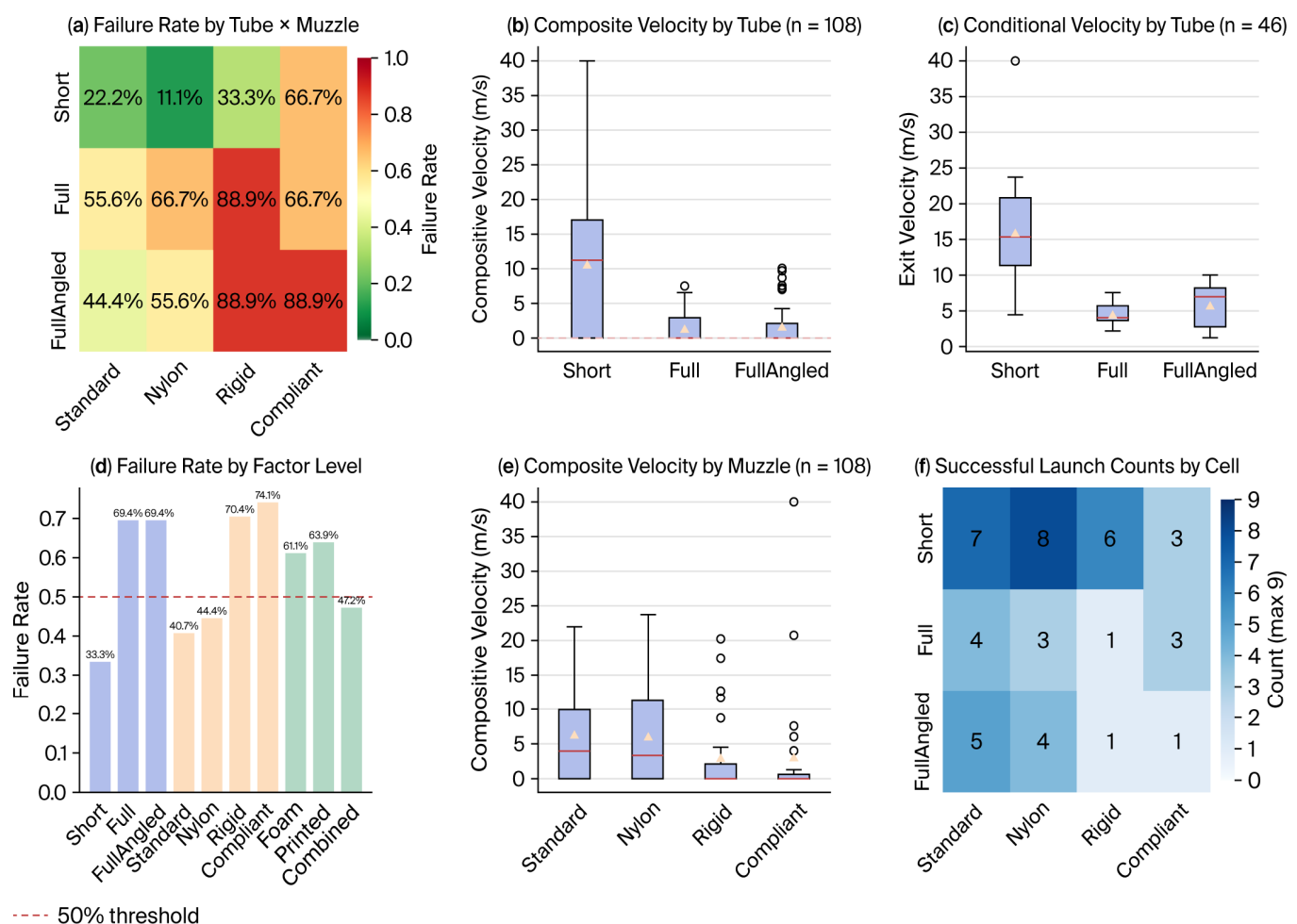


Figure 4. Analysis of exit velocity and failure rates across experimental conditions. (a) Failure rate heatmap by tube and muzzle configuration. (b) Composite velocity (all 108 observations) by tube. (c) Conditional exit velocity (successful launches only) by tube. (d) Failure rate bar chart by factor level; dashed line denotes 50% threshold. (e) Composite velocity by muzzle. (f) Successful launch counts by cell (maximum = 9).

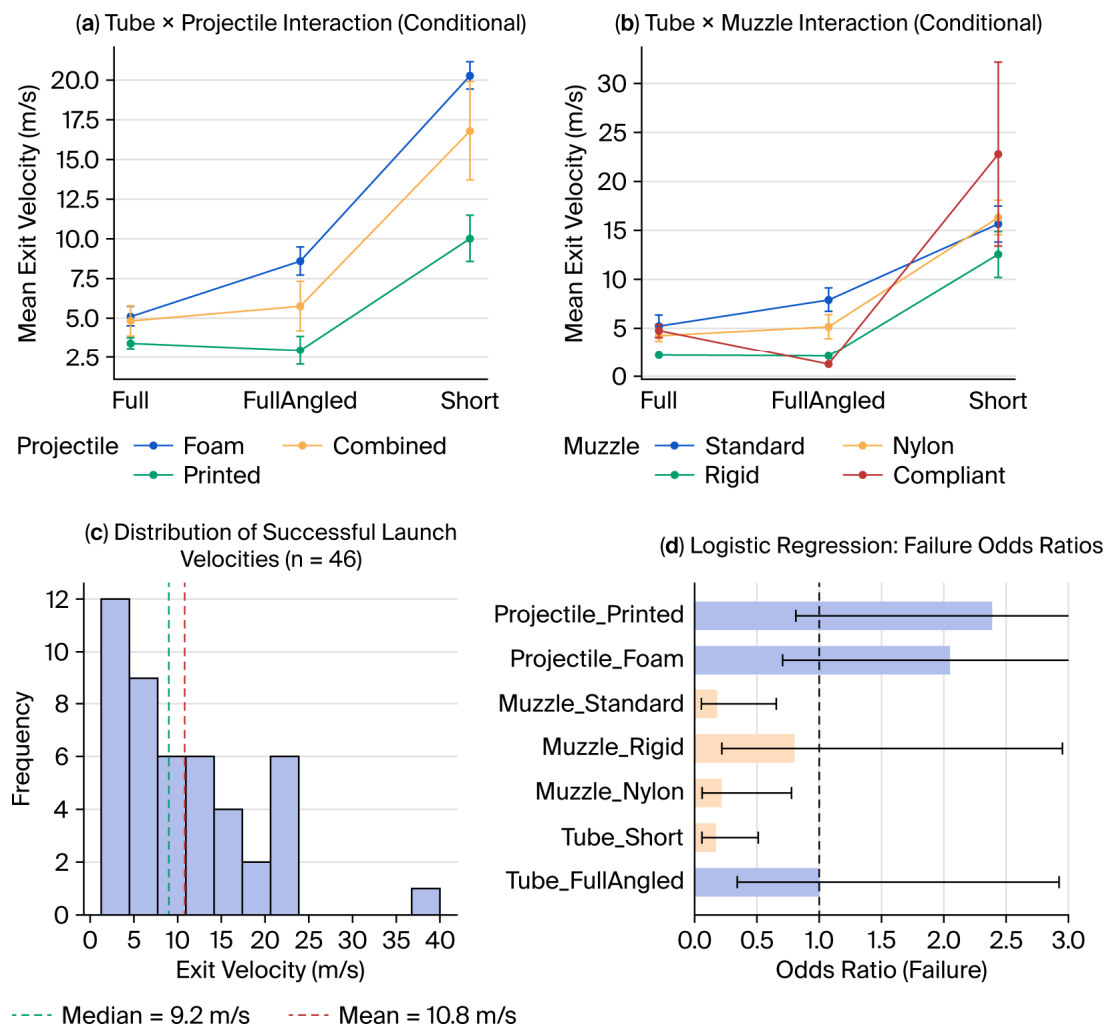


Figure 5. Interaction and diagnostic plots. (a) Tube × Projectile interaction plot (conditional mean velocity $\pm$ SEM). (b) Tube × Muzzle interaction plot (conditional mean velocity $\pm$ SEM). (c) Distribution of successful launch velocities with mean and median indicated. (d) Logistic regression odds ratios for failure probability with 95% confidence intervals; vertical dashed line at OR=1.

The Full tube configuration—behaving as a prestressed, quasi-statically rigid extended barrel—exhibited a failure rate identical to the Full Angled configuration (69.4% vs. 69.4%; logistic regression OR = 1.00, $p = 1.000$). This parity indicates that elevated failure rates stem from barrel extension and restricted radial clearance rather than off-axis geometry or tensegrity-specific vibrational compliance, though transient dynamic stiffness during the 10 ms pneumatic impulse was unmeasured. The acrylic Rigid Control tube confirms this driver: it yielded the same failure taxonomy (bore constriction and muzzle lodging) at equivalent rates, demonstrating that geometric impedance (160 mm barrel length, 1.65 mm radial clearance) governs failure rather than tensegrity compliance. Thus, the Full Extension tube serves as an effective control, isolating bore impedance from architectural compliance.

Muzzle geometry significantly constrained reliability (chi-square = 9.846, $df = 3$, $p = 0.020$). The Standard smoothbore yielded the lowest failure rate (40.7%, 11/27), followed by Nylon soft rifling (44.4%, 12/27), Rigid plastic rifling (70.4%, 19/27), and Compliant printed rifling (74.1%, 20/27). Projectile type had no significant effect (chi-square = 2.348, $df = 2$, $p = 0.309$). Due to the Resolution III design and cell imbalance, interaction effects remain exploratory; however, Full tube extensions paired with Rigid and Compliant muzzles failed in 88.9% and 66.7% of shots, respectively.

As shown in Tables 2 and 3, logistic regression reinforced these effects. Relative to the Full tube baseline, the Rigid Control tube significantly reduced failure odds (OR = 0.17, 95% CI [0.05, 0.50], $p = 0.002$). The Full Angled tube showed no significant deviation (OR = 1.00, $p = 1.000$), confirming that off-axis geometry provides no reliability gain under prestressed rigidity. Compared to the Compliant muzzle reference, failure odds dropped significantly with the Standard smoothbore (OR = 0.22, $p = 0.009$) and Nylon soft rifling (OR = 0.22, $p = 0.019$), whereas Rigid rifling showed no improvement (OR = 0.80, $p = 0.740$). Projectile type remained non-significant across all models (all $p > 0.18$).

Table 2. Logistic regression results for launch failure probability ($n = 108$). Reference categories: Full tube, Compliant muzzle, Combined projectile.

Parameter	Beta	Std. Error	Odds Ratio	z	Wald-Chi ²	p-Value	95% CI Lower	95% CI Upper
Intercept	1.29	2.38	3.63	1.97	3.88	0.048	1.01	13.07
Tube: Rigid Control	−1.77	0.10	0.17	−3.16	9.99	0.002	0.05	0.50
Tube: Full Angled Tensegrity	0	0.55	1.00	0.00	0	1.000	0.34	2.93
Muzzle: Standard	−1.51	0.14	0.22	−2.61	6.81	0.009	0.06	0.77
Muzzle: Nylon	−1.51	0.15	0.22	−2.35	5.52	0.019	0.06	0.80
Muzzle: Rigid	−0.22	0.53	0.80	−0.33	0.11	0.740	0.22	2.93
Projectile: Printed	0.87	1.31	2.39	1.59	2.53	0.112	0.82	7.00

Table 3. FMEA criticality rankings.

Failure Mode	Severity	Occurrence	Detection	Criticality	% of Total Criticality
Type 2 (Bore constriction)	8	7	6	336	54.8%
Type 1 (Muzzle lodging)	8	6	4	192	31.3%
Type 3 (Rifling obstruction)	8	4	3	96	13.9%

3.5. Composite Velocity Analysis (Full Factorial, $n = 108$)

To mitigate survivorship bias, a composite velocity score (0 m/s for failures, observed velocity for successes; $n = 108$) was evaluated. A Kruskal–Wallis test identified tube configuration as the primary determinant of performance ($H = 26.73$, $df = 2$, $p < 0.001$). Rigid Control tubes achieved a median composite velocity of 12.4 m/s, whereas Full and Full Angled configurations yielded medians of 0.0 m/s. Muzzle geometry was also statistically significant ($H = 10.20$, $df = 3$, $p = 0.017$), with Standard and Nylon muzzles outperforming Rigid and Compliant variants. Projectile type had no significant effect ($H = 2.82$, $df = 2$, $p = 0.244$). These results indicate that tube geometry and muzzle design dictate overall system effectiveness when treating failures as worst-case outcomes rather than missing data.

Process capability analysis of the Rigid Control composite velocity distribution relative to the 15 m s^{−1} LSL yielded $C_p = 1.05$ and $C_{pk} = 0.79$, indicating that even the best-performing configuration is not yet process-capable ($C_{pk} < 1.0$) due to high variability and zero-inflation from failures.

3.6. Conditional Exit Velocity (Successful Launches Only, $n = 46$)

Exit velocity varied substantially across successful launches. Marginal means (mean $\pm$ SD in m/s) by projectile type were Foam at 14.49 ± 7.25 , Combined at 10.71 ± 8.78 , and Printed at 6.84 ± 4.54 . By tube configuration, means were Rigid Control at 15.96 ± 7.29 , Full Angled Tensegrity at 5.73 ± 3.25 , and Full Tensegrity at 4.49 ± 1.64 . By muzzle configuration, means were Standard at 10.59 ± 5.86 , Nylon at 10.89 ± 7.04 , Compliant at 11.92 ± 13.91 , and Rigid at 9.89 ± 6.83 . The peak velocity of 36.1 m/s (Combined projectile, Rigid Control tube, Compliant muzzle) was replicated at 35.8 m/s, establishing the pneumatic performance ceiling under minimal barrel impedance. This confirms that launcher energy capacity meets the design envelope and attributes reduced velocities in extended setups to barrel-induced energy dissipation. The minimum non-zero velocity was 1.3 m/s (Printed projectile, Full Angled tube, Compliant muzzle).

Non-parametric Kruskal–Wallis tests on conditional velocity corroborated these findings. Tube configuration was statistically significant ($H = 29.61$, $df = 2$, $p < 0.001$), projectile type was significant ($H = 7.28$, $df = 2$, $p = 0.026$), and muzzle configuration was not ($H = 0.665$, $df = 3$, $p = 0.881$). Mann–Whitney U tests confirmed significant tube-length effects with large effect sizes: Rigid Control versus Full ($U = 260.0$, $p < 0.001$, Cohen's $d = 1.91$) and Rigid Control versus Full Angled ($U = 250.0$, $p < 0.001$, Cohen's $d = 1.65$).

3.7. Cell Imbalance and Statistical Power

Table 4 summarizes successful launch counts across the 36 factorial treatment combinations. Data sparsity was severe: six cells yielded ≤ 1 successful launch, only eight achieved all three replicates, and two combinations—Full/Rigid and Full Angled/Compliant—produced just one success out of nine attempts.

This severe cell depletion precludes testing the three-way interaction. In parametric models, non-significant interaction terms likely reflect Type II errors caused by sparsity rather than a true absence of effect. Consequently, interaction analyses and the stratified evaluations in Section 4.5 are strictly exploratory. Furthermore, the conditional velocity analysis ($n = 46$) serves as an exploratory, secondary endpoint to the primary composite-score analysis ($n = 108$), which maintains adequate power for omnibus non-parametric inference.

Overall, these success rates align with a screening design, limiting robust statistical inference to main effects and large pairwise contrasts.

Table 4. Number of successful launches by tube, muzzle, and projectile (maximum per cell = 3).

Tube	Muzzle	Foam	Printed	Combined
Rigid Control	Standard	2	2	3
Rigid Control	Nylon	3	2	3
Rigid Control	Rigid	2	2	2
Rigid Control	Compliant	1	1	1
Full Tensegrity	Standard	1	1	2
Full Tensegrity	Nylon	1	1	1
Full Tensegrity	Rigid	0	0	1
Full Tensegrity	Compliant	1	1	1
Full Angled Tensegrity	Standard	2	1	2
Full Angled Tensegrity	Nylon	1	1	2
Full Angled Tensegrity	Rigid	0	0	1
Full Angled Tensegrity	Compliant	0	1	0

3.8. Statistical Summary

To evaluate projectile-tube interactions, separate Kruskal–Wallis tests were conducted within each tube configuration. In the Rigid Control tube ($n = 24$), projectile type significantly affected conditional exit velocity ($H = 12.45$, $df = 2$, $p = 0.002$), with Foam and Combined projectiles exceeding printed cylinders' performance. Conversely, projectile type and muzzle rifling had no significant effect within the Full ($n = 11$) or Full Angled ($n = 11$) configurations (all $p > 0.14$).

These results indicate that projectile-specific velocity dynamics are detectable only when the launcher operates reliably. When structural tube performance acts as the primary system constraint, tube failure modes mask projectile properties. The stratified tests for the Full and Full Angled configurations are statistically underpowered; they document effect sparsity rather than establish definitive null effects.

4. Discussion

4.1. Summary

The systematic integration of tensegrity launch tubes, compliant mechanisms, and variable rifling into a small-scale line launcher introduces critical reliability bottlenecks that outweigh any mechanical versatility benefits. Evaluation demonstrates that launch failure rate—not exit-velocity modulation—is the primary metric differentiating barrel configurations. Just as reported in prior literature, barrel occlusion remained the highest cause of failure [5–7]. Rigid Control barrels (no extension) achieved a 67% success rate and the highest mean exit velocity (15.96 m/s). Conversely, Full Extension tensegrity tubes failed in 69.4% of launches, yielding significantly lower exit velocities (4.49 m/s). Parity between Full and Full Angled failure rates (both 69.4%) indicates that failures stem from extended barrel length and insufficient radial clearance (1.65 mm per side), rather than vibrational compliance or angular misalignment. Improving clearance could improve performance to match existing tensegrity masts [5–7]. The acrylic Rigid Control tube—sharing identical length and clearance without tensegrity architecture—exhibited equivalent failure modes and rates, confirming that impedance mismatch and friction drive failure. Consequently, extended tubes induced catastrophic launch failures in nearly 70% of trials, contradicting the hypothesis that tensegrity extensions manage recoil while preserving alignment.

Projectile mechanics are secondary to barrel reliability. Although projectile type was non-significant in the conditional omnibus model, stratified analysis within the Rigid Control configuration showed significant effects (Kruskal–Wallis $H = 12.45$, $p = 0.002$), where Foam and Combined projectiles outperformed Printed cylinders. Projectile compressibility and mass influence energy transfer efficiency only when the barrel does not restrict launch. In extended configurations, tube failure modes mask projectile-level variations; statistical analyses for these configurations remain underpowered and exploratory.

Muzzle rifling failed to yield the hypothesized velocity-stability trade-off. Compliant printed rifling exhibited the highest failure rate (74.1%), followed by Rigid plastic (70.4%). Smoothbore (40.7%) and soft Nylon (44.4%) were substantially more reliable. Mean exit velocities among successful launches did not differ significantly by muzzle type, indicating that aerodynamic benefits were offset by frictional losses and increased failure probability. These findings challenge the idea that DfAM geometric complexity is inherently advantageous. The potential trade-offs require future optimization to match the performances reported in existing literature [5–7].

FMEA and Taguchi loss functions highlight these process risks. Bore constriction (Type 2) dominated occurrence (45.2%) and criticality (54.8%), marking it as the primary redesign target. Under Taguchi loss modeling, a single failure incurs approximately $244 \times$

the economic loss of a marginal success at the Rigid Control velocity, proving that reliability must take precedence over velocity optimization in Stage 2 development.

Economic and process assessments further underscore system immaturity. While raw material cost was under \$15 USD across a 48 h print cycle, Total Cost of Ownership (TCO)—factoring equipment utilization (\$240–384 USD), manual tensioning labor (\$112.50 USD), and scrap-adjusted material inflation ($2.35\times$ per success)—yields an actual cost of \$8.07–11.22 USD per successful launch. Monolithic printed components reduced assembly labor, but manual post-processing yielded only a net 60–65% labor reduction and a 65–70% system-level part reduction. Finally, process capability analysis shows negative Cpk for failure rate (-0.20 vs. 5% upper specification limit) and near-zero Cpk for exit velocity (0.04 vs. 15 m/s lower specification limit), proving the prototype fails to meet foundational stage-gate targets.

4.2. Mechanistic Failure Hypotheses

High failure rates in extended tube and rifled configurations stem from three primary mechanisms supported by post-test failure mode taxonomy and FMEA criticality rankings. First, the Full and Full Angled Tensegrity tubes (160 mm length, 16 mm bore) provide 1.65 mm radial clearance per side for 12.7 mm projectiles, violating the ≥ 2 mm minimum requirement for low-impulse pneumatic launchers. Segmental twist or thermal deformation induces intermittent contact and projectile lodging (Type 2 failures: 45.2% of occurrences, Criticality = 336). Identical lodging in the jointless acrylic Rigid Control tube confirms that geometric clearance deficiency, rather than tensegrity compliance, drives this failure mode. Second, rifling attachments increase bore resistance and energy-absorbing surface area, damping the pneumatic pulse prior to full projectile acceleration (Type 3 failures: 16.1% of occurrences). Third, a series of reliability block diagrams yields $R_s = R_{\text{tube}} \times R_{\text{muzzle}} = 0.31 \times 0.60 = 0.19$, aligning with observed failure rates and isolating the tube as the single point of failure.

To resolve these failure modes, future redesigns must increase radial clearance to ≥ 2 mm and incorporate stiffer tension members such as Kevlar or carbon fiber to limit dynamic constriction. Continuous rifling should be replaced with short (≤ 10 mm) silicone or TPU muzzle brakes to induce spin without severe pressure loss. Finally, topology optimization algorithms should integrate minimum clearance and maximum allowable contact force as hard constraints alongside energy storage metrics.

4.3. Scaling Roadmap

The present study focuses on the micro-launcher scale (projectile mass < 2 g, exit velocity < 40 m s $^{-1}$, pneumatic impulse < 5 J). Scaling to industrial applications requires a staged roadmap:

Stage 1 (Present Work): Establishes Design for Additive Manufacturing (DfAM) reliability benchmarks and a failure-mode taxonomy using commodity FDM and PLA. Quantitative gate criteria for prototype-to-pilot transition—failure rate $\leq 5\%$, mean exit velocity ≥ 15 m s $^{-1}$, and coefficient of variation (CV) $< 20\%$ —are derived from the upper performance bounds of the pneumatic benchmark (Rigid Control: 66.7% reliability, mean velocity 15.96 m s $^{-1}$). The 15 m s $^{-1}$ velocity floor and 20% CV cap represent the lower 95% confidence interval of successful control launches. Current process capability analysis indicates the prototype does not yet meet these criteria (Cpk < 1.0). Operational thresholds (e.g., SOLAS) are deferred to Stages 4–5 [3].

Stage 2: Scales bore diameters to 25–50 mm and projectile masses to 100–500 g using engineering polymers (PETG, ABS, Nylon) or SLM Ti6Al4V. Transient pressure wave and contact force FEA simulations are integrated pre-build to verify radial clearances.

Stage 3: Integrates automated tensioning and single-build tensegrity fabrication to eliminate manual prestress variation, alongside in-barrel pressure transducers and high-speed videography (≥ 1000 fps) for diagnostic tracking. System evaluation transitions from fixed-sample classical testing to reliability growth modeling (e.g., Duane–Crow AMSAA) and sequential Bayesian updating [3].

Stage 4: Advances to field testing once laboratory reliability exceeds 95% over 100 consecutive cycles. System analysis incorporates reliability block diagrams, fatigue-life modeling, and fault tree analysis (FTA) [3].

Stage 5: Achieves SOLAS and aerospace qualification through environmental testing (salt spray, UV, thermal cycling) and third-party certification [9,16].

Stage 6: Addresses Maritime SOLAS compliance and aerospace qualification that require environmental testing (e.g., salt spray, UV, thermal cycling) and third-party certification [9,16]. The present manuscript provides the quantitative prototyping benchmarks that must be met at each stage before progression.

4.4. Limitations

Several study limitations warrant consideration. First, conditional velocity analysis was constrained to 46 successful launches (a 57.4% failure rate) from the 108 full-factorial observations, reducing statistical power and elevating Type II error risk for high-order interactions. Nevertheless, large effect sizes (Cohen's $d > 1.6$) and parametric/non-parametric convergence confirm the primary findings. Sub-analyses within Full and Full Angled configurations remain exploratory [22].

Second, high failure rates demonstrate that current DfAM geometries are unsuited for the applied pneumatic impulse. Both configurations—despite behaving as quasi-statically rigid under prestress—introduced excessive barrel length and insufficient radial clearance. An identical failure profile in the rigid acrylic control tube confirms that bore impedance is a geometric phenomenon rather than a structural compliance artifact. Redesign requires larger bore clearances (≥ 2 mm radial) and higher-stiffness tension members (e.g., Kevlar, carbon fiber). FMEA confirms bore constriction (Type 2, 45.2%) as the dominant failure mechanism over launcher energy deficit [25].

Third, using a single material (PLA, $T_g \sim 60$ deg C) on a desktop FDM platform constrains the operational envelope. Industrial deployment requires engineering polymers (PETG, ABS) or metallic additive manufacturing (SLM, Ti6Al4V) [23]. While commodity hardware maximized accessibility in Stage 1, it limits operational generalizability [23–25].

Fourth, the chronograph's 1.2 m/s detection threshold precluded distinguishing low-velocity exits (0–1.2 m/s) from true lodged-projectile failures, though post-test disassembly confirmed mechanical obstruction in all cases. High-speed videography (≥ 1000 fps) is recommended for Stage 2.

Fifth, aerodynamic trajectory analysis was restricted to a single station at 0.5 m. Aerodynamic benefits are secondary until mechanical reliability exceeds 95%. The observed 36.1 m/s velocity ceiling confirms launcher energy capacity but does not indicate flight stability.

Sixth, manual tensegrity assembly introduced prestress variability, though inter-block verification mitigated this effect. While automated weaving or monolithic fabrication [4] would enhance repeatability, geometric redesign must precede automation [12]. Desiccant conditioning effectively controlled moisture (mean 0.12%), but batch-to-batch PLA hygroscopicity was unquantified.

Seventh, quasi-static rigidity claims for the Full tube rely on static compression and visual data; transient dynamic stiffness during the 10 ms impulse may differ. However, because the acrylic control tube exhibited congruent failure taxonomies without a tension

network, tensegrity-specific dynamic compliance is likely minimal [13–15], supporting the bore impedance hypothesis.

Eighth, the absence of high-speed cameras limits standard analytical approaches common in tensegrity literature [5–7]. Without high-speed video capture, validating finite element analysis (FEA) and other computational models becomes difficult [5–7]. Furthermore, insufficient video resolution hinders precise error analysis and limits experimental replicability.

4.5. Future Work

Future research must prioritize reliability engineering over geometric novelty. Immediate efforts should focus on transient pressure-wave and projectile-tube contact force root-cause FEA to guide redesigns featuring increased clearances and stiffer strut geometries. While multi-material additive manufacturing (AM) [4,6,8] can eliminate manual assembly, material selection must prevent creep under pneumatic launch loads. Muzzle rifling requires re-engineering; integrating silicone or TPU threads into a short (≤ 10 mm) muzzle brake may induce spin without triggering catastrophic failure.

Additionally, topology optimization [12,15] must incorporate reliability constraints—specifically maximum allowable contact force and minimum bore clearance—rather than optimizing solely for energy storage. Field testing should remain deferred until laboratory reliability exceeds 95%. Standardized design frameworks for scalable compliant mechanisms [3] must integrate fatigue-life models, reliability block diagrams, and fault tree analysis from initiation. Finally, Stage 2 MSA should incorporate in-barrel pressure transducers and high-speed videography to ensure unambiguous failure classification and reliable process capability index estimation [23,25].

5. Conclusions

This Stage 1 proof-of-concept evaluation of FDM-printed tensegrity launch tubes and compliant mechanism launchers demonstrates that while additive manufacturing of metamaterials is theoretically compelling, significant engineering barriers remain before industrial scaling. The experimental program revealed a high overall failure rate of 57.4%, driven primarily by geometric bore impedance—insufficient radial clearance and excessive barrel length—rather than tensegrity-induced vibration. FMEA identified bore constriction as the primary criticality driver (54.8% of cumulative criticality), while a Taguchi loss function quantified that a complete launch failure incurs 244 times the economic loss of a marginal velocity deviation. Consequently, process capability analysis yielded negative to near-zero Cpk values, failing to meet Stage 1 gate criteria (failure rate $\leq 5\%$, mean velocity ≥ 15 m s^{−1}). When accounting for equipment utilization, manual assembly, and scrap-adjusted material inflation, the effective cost rises to US\$8.07–11.22 per successful launch, rendering the system unviable for SOLAS-regulated rescue or aerospace applications. Additionally, the compliant PLA launcher exhibited a 26-fold kinetic energy deficit compared to pneumatic benchmarks, and printed rifling configurations actively exacerbated bore obstruction. Collectively, these findings mandate that Stage 2 redesign priorities invert conventional DfAM paradigms by prioritizing reliability engineering, minimum bore clearance specifications, and contact-force constraints over geometric novelty. To progress along the proposed five-stage scaling roadmap, future research must focus on transient projectile-tube contact mechanics, automated single-build fabrication to eliminate manual prestress variability, and the integration of reliability constraints into topology optimization protocols to achieve the 95% laboratory reliability threshold required for operational field environments.

Supplementary Materials: The following supporting information can be downloaded at: <https://www.mdpi.com/article/10.3390/industries1010007/s1>, the data, models, and supplementary information (filenames include: otherFiles.zip, qingLongFiles.zip, qingLongEndcaps.zip, README.md) used in this launcher.

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