

Design of a novel thin-walled tube specimen for multiaxial loading conditions and a method to estimate the load-train compliance

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ABSTRACT

Additive manufacturing, specifically fused filament fabrication (FFF), is increasingly utilized for end-use parts, but it presents challenges such as weak interlayer bonding and therefore highly anisotropic mechanical behaviour. While the axial (tensile and compressive) mechanical response of 3D-printed polymer parts is well-documented, their response to static or cyclic combined (axial and torsional) loads remains relatively unexplored. This study focuses on designing a novel thin-walled cylindrical specimen geometry for 3D-printed acrylonitrile butadiene styrene (ABS) suitable for testing under tension, compression, torsion, and combined loading conditions. To mitigate structural testing challenges like clamp slippage and premature failure, the specimen incorporates fully dense grip regions, longitudinal surface grooves, and stress-relief transition zones. Because the specimen's size prevented the use of standard extensometers, the study additionally proposes and validates a methodology to correct for load-train and machine compliance. Findings demonstrate that the multiple-length (ML) method yields highly accurate corrections for calculated elastic properties compared to the rigid specimen (RS) method. For example, the ML compressive modulus was estimated at 1685 ± 24 MPa, much closer to the measured elastic modulus of 1731 ± 16 MPa obtained through standard tensile tests, and higher than the value calculated with RS (1185 ± 12 MPa). Furthermore, mechanical analysis confirms that structural failure of these specimens at 47.3 MPa under compression (2.5 times the tensile load) is driven by material densification and plastic collapse rather than geometric buckling. Ultimately, this research establishes a robust, highly accessible, and low-cost framework for accurately evaluating the macroscopic multiaxial properties of 3D-printed polymers, overcoming the limitations of traditional solid-bar specimens and expensive non-contact strain measurement systems.

1. Introduction

Additive manufacturing (AM) has experienced an exponential growth across various industries, including aerospace, automotive, biomedical, and civil engineering, due to its flexibility in producing complex, near-net-shaped components without the need for auxiliary tools or moulds. This capability minimizes material waste, making AM a highly attractive option for rapid prototyping, design development, and low-volume manufacturing [1–5]. Within this domain, fused filament fabrication (FFF) is one of the most widely adopted techniques. While traditionally used for prototyping, FFF increasingly demonstrates strong potential as a cost-effective method for producing end-use parts [6]. Despite its versatility, FFF presents distinct challenges compared to other AM techniques, such as vat photopolymerization (VP) and selective laser sintering (SLS). It generally features lower resolution and

slower production rates. More critically, FFF parts often manifest highly anisotropic mechanical properties. The layer-by-layer deposition process inherently leads to weak interlayer bonding and void formation, which significantly reduces the strength and toughness of printed parts, particularly along the build direction (z-axis) [4,7]. Understanding the behaviour of FFF polymers under complex multiaxial loads is essential to successfully transition these materials into functional, load-bearing applications. Particularly in the aerospace, automotive, and biomedical sectors, components such as unmanned aerial vehicle (UAV) airframes, automotive drive shafts, robotic linkages, and orthopaedic implants routinely experience coupled axial and torsional stresses during their service life [4,8,9]. However, current literature heavily favours tensile and compressive studies, leaving the response to static or cyclic torsional loads relatively unexplored [10]. Furthermore, the limited research investigating the torsional behaviour of 3D-printed specimens

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predominantly focuses on solid bars [5,6] and cylinders subjected to either quasi-static [10,11] or cyclic loads [12–15] but, utilizing thin-walled cylinders is structurally the most efficient way to maximize torsional resistance while minimizing material waste. Yet, research on 3D-printed thin-walled tubes remains scarce [16]. While a notable recent study has successfully employed thin-walled tubular specimens to characterize the combined tensile-shear behaviour of polyamide-12 manufactured via laser powder bed fusion (LPBF) [17], most existing studies focusing instead on metals [18–21] or continuous fiber-reinforced composites, where studies have primarily focused on energy absorption capability [22], torsional fatigue resistance [23], torsional or axial properties [24,25] and multiaxial loading (axial plus internal pressure) [26,27].

This study aims to design a novel cylindrical specimen geometry suitable for uniaxial and multiaxial testing under tension, compression, torsion, or combined loading conditions. Furthermore, the paper addresses the impact of machine and specimen compliance on mechanical measurements and proposes a methodology to correct the calculated elastic constants for FFF specimens without the use of complex and expensive digital image correlation systems. This is particularly critical when the specimen's complex geometry or incompatibility with the available instrumentation prevents the use of an extensometer.

This research focuses on acrylonitrile butadiene styrene (ABS) due to its widespread use in FFF [2,28,29]. Furthermore, as an amorphous polymer, ABS eliminates the effects and defects typically associated with crystallinity (such as warpages). To the best of the authors' knowledge, this study represents the first successful adaptation of thin-walled tubular mechanics specifically designed to overcome the unique constraints of FFF-printed ABS under multiaxial loads.

These findings will serve as the baseline for two subsequent studies: one utilizing this validated methodology to investigate the influence of different materials and raster orientations, and another aimed at developing a novel failure criterion for 3D-printed polymers under quasi-static multiaxial loading conditions.

2. Materials, design and processing

2.1. Material

The thermoplastic filament used in this study was a Bambu Lab® acrylonitrile butadiene styrene (ABS Bambu Green 40500) in green colour. Complementary physical (Table 1) and mechanical properties (Table 2) taken from the technical datasheet [30] are reported.

2.2. Specimens design

Initially, standard geometries, such as an axisymmetric revolution of the ISO 527 1A tensile specimen, were considered. However, these were ultimately discarded. In fact, their small cross-sectional area generated insufficient axial and torsional loads on the testing equipment, resulting in a low signal-to-noise ratio, particularly within the elastic region of the material response. Furthermore, the high slenderness ratio of these standard geometries made them highly susceptible to buckling phenomena during compression testing.

Table 1

Physical properties of ABS filament, measured by the authors. * Helium Pycnometer Accupyc 1330. ** Mettler DSC30, inflection point in the first heating scan at 10 °C/min *** Kayeness Dynisco 4002 MFR plastometer, according to ASTM D1238-20.

Parameter	Value
Average diameter (D_m)	1.73 ± 0.01 mm
Bulk density*	1.062 ± 0.001 g/cm ³
Linear density	2.47 ± 0.02 g/m
T _g **	95.6 °C
Melt Flow Index (260 °C, 2.16 kg)***	34 ± 4 g/10 min

Table 2

3D-printed Bambu Lab® Green ABS mechanical properties taken from the technical datasheet [30].

Subjects	Testing Methods	Data
Young's Modulus (X-Y)	ISO 527, GB/T 1040	2200 ± 190 MPa
Young's Modulus (Z)	ISO 527, GB/T 1040	1960 ± 110 MPa
Tensile Strength (X-Y)	ISO 527, GB/T 1040	33 ± 3 MPa
Tensile Strength (Z)	ISO 527, GB/T 1040	28 ± 2 MPa
Breaking Elongation Rate (X-Y)	ISO 527, GB/T 1040	$10.5 \pm 1.0\%$
Breaking Elongation Rate (Z)	ISO 527, GB/T 1040	$4.7 \pm 0.8\%$
Bending Modulus (X-Y)	ISO 178, GB/T 9341	1880 ± 110 MPa
Bending Modulus (Z)	ISO 178, GB/T 9341	1590 ± 100 MPa
Bending Strength (X-Y)	ISO 178, GB/T 9341	62 ± 4 MPa
Bending Strength (Z)	ISO 178, GB/T 9341	39 ± 4 MPa
Impact Strength (X-Y)	ISO 179, GB/T 1043	39.3 ± 3.6 kJ/m ²
Impact Strength (Z)	ISO 179, GB/T 1043	7.4 ± 1.2 kJ/m ²

The specimen geometry for these tests was adapted from the design guidelines proposed by Benedetti et al. [31] (Fig. 1a), who conducted biaxial tests on thin-walled cast iron cylinders. A large external diameter of 20 mm and a thickness of 2 mm (as in Benedetti's work) were selected to ensure the generation of sufficiently high torque and axial forces during testing. This design choice effectively increases the signal-to-noise ratio of the acquired data and helps preventing structural buckling. The gauge length was reduced from the 50 mm used in Benedetti's study to 30 mm. This optimization minimizes material consumption and printing time while remaining sufficiently long to ensure that stress flow lines are not disturbed and the stress distribution is homogeneous without being affected by the grip zones. The structural resistance to elastic buckling of the chosen geometry was analytically validated using Equation S(1) through S7.

Regarding the clamping mechanism, Benedetti et al. utilized a metallic pin to prevent the grip zone from collapsing under clamp pressure. A similar internal cylindrical pin was initially designed for this study; however, preliminary tests revealed that clamping still induced excessive local deformation, leading to premature failure near the grips. Furthermore, the inherently low surface friction of the 3D-printed ABS, combined with the circular cross-section of the grip region, resulted in significant clamp slippage during both axial and torsional loading. To overcome these experimental challenges, a series of design optimizations were implemented:

- 100% infill in grip regions: the clamping zones were printed with a fully dense infill to withstand gripping pressure and prevent the excessive deformations that cause premature failure.
- Optimized clamping radius: the radius of the clamping zone was reduced to 24 mm to balance adequate gripping surface with material and print-time efficiency.
- Surface grooves: longitudinal grooves were incorporated into the clamping zones to maximize the contact area with the machine grips, effectively eliminating slippage in all loading modes.
- Stress-relief section: a 5 mm straight section was introduced between the grip zones and the fillet region to prevent the stress concentrations induced by the clamps from affecting the gauge length.
- Smooth transitions: a large internal fillet radius of 15 mm was selected to ensure a gradual stress distribution. To maintain a consistent external fillet radius alongside this internal geometry, a chamfer section was added.

An alternative design featuring a clamping zone radius of 20 mm (matching the gauge section) was also considered to further reduce material usage. However, this geometry was ultimately discarded. During the printing process, the internal geometric transition created slight external surface imperfections. These defects acted as stress concentrators, ultimately triggering premature specimen failure near the clamping region. Fig. 1b reports the final optimized specimen design, alongside its detailed technical drawings in Fig. 1c.

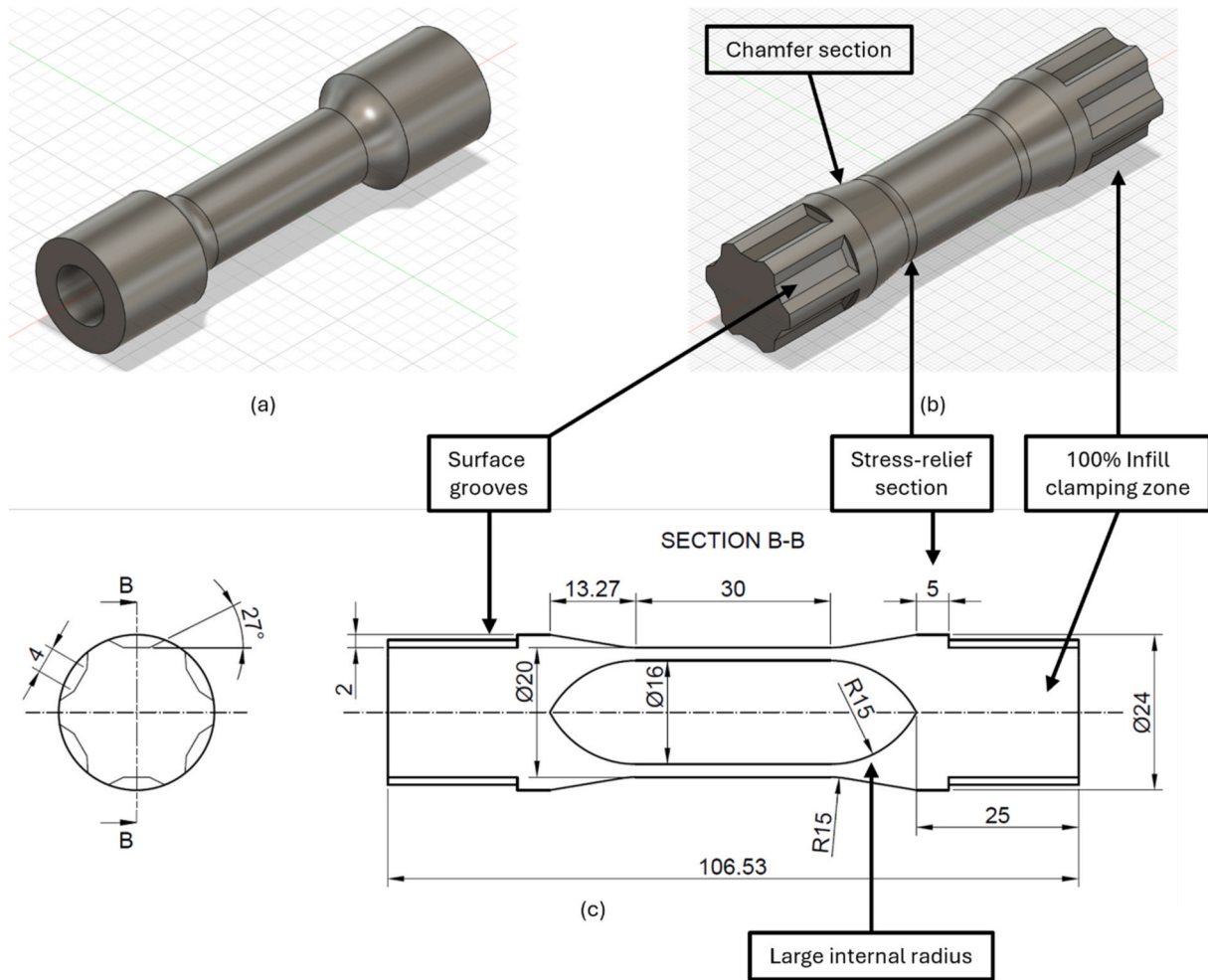


Fig. 1. (a) Specimens used by Benedetti et al. [31] for biaxial tests on cast iron. (b) Optimized specimens used in this study. (c) technical drawings of (b). Values are in mm.

2.3. Material processing and specimens production

The fabrication of the specimens by FFF was carried out using a Bambu Lab® X1E with a 0.4 mm nozzle, employing BambuStudio's '0.20 mm Standard @BBL XC1' preset with 100% concentric shell and infill [32] (detailed printing parameters and additional data are reported in Table S1 and S2). Three specimens were printed oriented vertically with respect to the build plate (Fig. 2) per each batch. Fig. 3 shows different sections of the specimen and how the printing speed is adjusted automatically by the slicing software.

ISO 527 1BA uniaxial tensile test specimens were also manufactured, in accordance with the standard, using the same 3D printing apparatus employed for the cylindrical specimens. These specimens were printed vertically with respect to the build plate to be consistent with the orientation of the cylindrical specimens (Fig. 2b). The tensile test on ISO 527 1BA specimens were used to compare the mechanical properties of the two different geometries and assure the validity of the compliance correction method.

The printing parameters were identical with two exceptions: the selected printing pattern was “aligned rectilinear” and the option “detect narrow internal solid infill” was unselected to avoid the automatic use of a concentric pattern (Fig. 4a), typically preferred by the software when the thickness of the object is particularly small. The “aligned rectilinear” pattern (Fig. 4b) is particularly crucial as it allows to have a continuous print for each layer avoiding the formation of small voids which the concentric pattern leaves when the tips stop the print to

shift to the inner or outer line (Fig. 4c). The specimens were printed 10 at a time.

3. Experimental techniques

3.1. Uniaxial tensile test

Quasi-static uniaxial tensile tests were conducted to evaluate the elastic modulus (E), Poisson's ratio (ν), ultimate tensile strength (σ_{Max}) and strain at break (ϵ_{Max}) of the materials using an Instron® 5989 tensile electromechanical universal testing machine equipped with a 10 kN load cell and a biaxial (axial/transversal) extensometer with a 12.5 mm axial gauge length. The tests were conducted at a strain rate of 1 mm/min on ten specimens ISO 527 type 1BA. The temperature and relative humidity of the testing room were 20 °C and 35% respectively. The evaluation of the elastic properties was conducted covering a deformation range from 0% to 0.4%. The elastic modulus was determined as the chord modulus across the 0.05–0.25% deformation range while the Poisson's ratio was calculated using Equation (1).

$$\nu = - \frac{\epsilon_{\text{transverse}}}{\epsilon_{\text{axial}}} \quad (1)$$

From the measurement of the elastic and Poisson moduli and assuming a linear elastic isotropic behaviour, the shear modulus (G) can be derived using Equation (2).

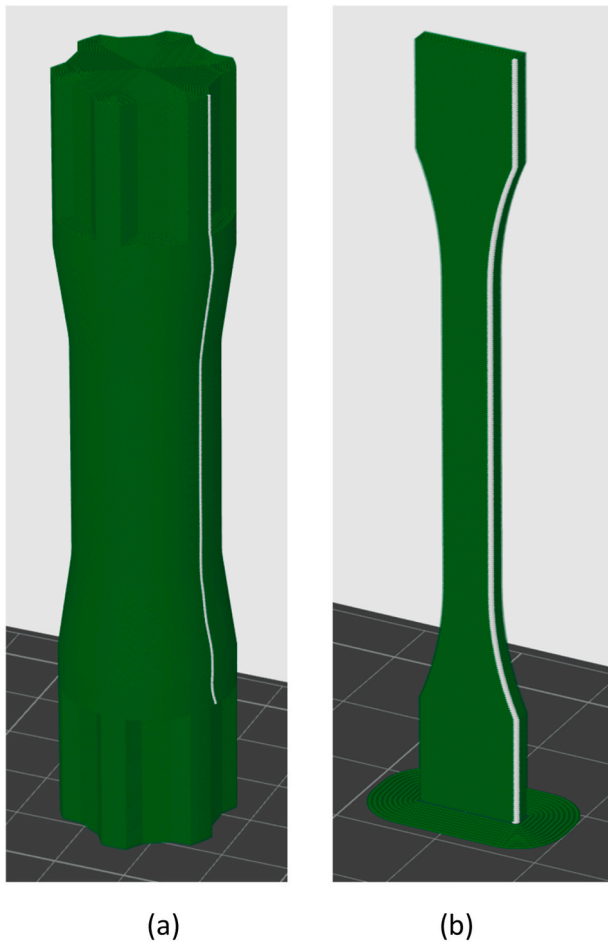


Fig. 2. (a) The novel cylindrical specimen, (b) ISO 527 1BA specimens for uniaxial tensile tests.

$$G = \frac{E}{2(1 + \nu)} \quad (2)$$

3.2. Axial and torsional test on cylindrical specimens

Quasi-static axial (tension and compression) and torsional tests were conducted using a Walter + Bai (Switzerland) LFV25-T75-HH axial/torsional servo-hydraulic universal testing machine, equipped with a biaxial load cell capable of 25 kN axial load and 75 Nm torque capacities. A pair of self-centring three-jaw lathe chucks was used to ensure a secure grip and precise specimen centring (Fig. 5). The tensile and compressive experiments were conducted by applying an axial displacement rate (ΔL) of 1 mm/min, ensuring consistency with uniaxial tensile testing on ISO 527 1BA specimens. The torsional tests were performed with an angular displacement rate of 60°/min. The temperature and relative humidity of the testing room were 20 °C and 35% respectively. Five cylindrical specimens were tested for each loading condition.

The biaxial testing machine provided raw data in the form of axial load (kN), stroke (mm), torque (Nm), and angular displacement (°). Therefore, assuming a thin-walled tube, a plane stress state and the use of an isotropic and linearly elastic material, Equations (3)–(6) were used for the calculation of the axial and shear strains (ϵ_{xx} and γ_{xy}) and stresses (σ_{xx} and τ_{xy}).

$$\epsilon_{xx} = \frac{\Delta L}{L_0} \quad (3)$$

$$\gamma_{xy} = \frac{R_m \cdot \phi}{L_0} \quad (4)$$

$$\sigma_{xx} = \frac{N}{A} \quad (5)$$

$$\tau_{xy} = \frac{T}{2 \cdot s \cdot A_m} \quad (6)$$

Where Φ is the torsion angle in radian, N is the applied load, T is the applied torque, A_m is the area enclosed by the midline of the cross-section and s is the thickness of the cylinder.

To perform these conversions the internal and external radii of the specimens were required. Since the internal dimensions of FFF-printed parts cannot be measured directly after fabrication, a representative

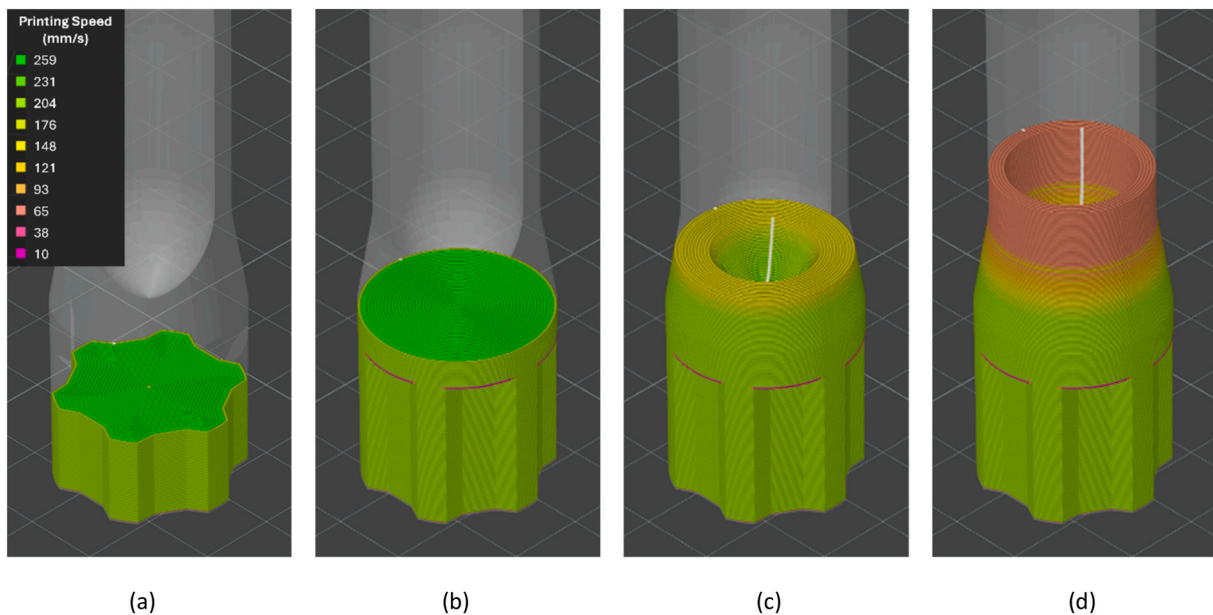


Fig. 3. Cylindrical specimen sliced with BambuStudio software at different layers: (a) 60, (b) 120, (c) 160, (d) 220. The colour represents different printing speeds, reported in Table S2.

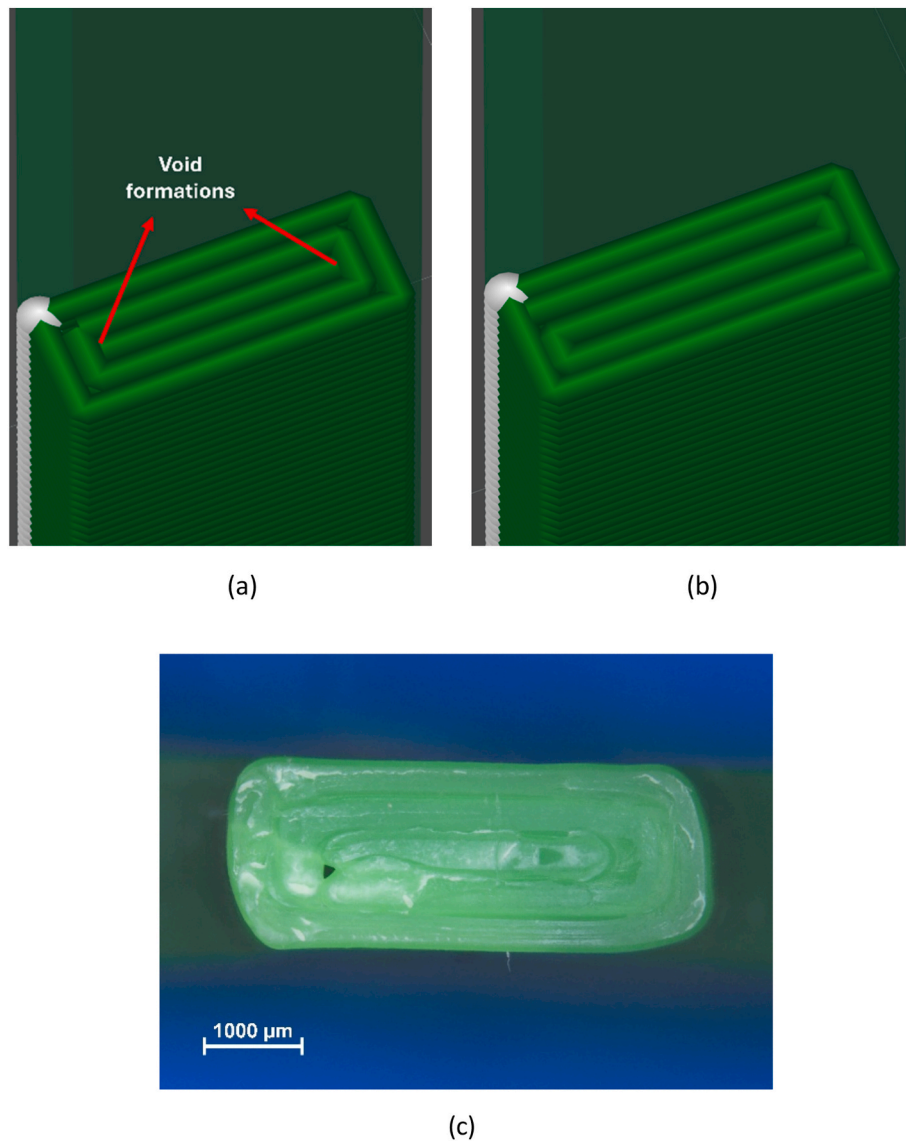


Fig. 4. (a) “Concentric” printing pattern, automatically activated when the option “detect narrow internal solid infill” is selected, and possible void formation zones, (b) “aligned rectilinear” pattern allows for a continuous print, (c) example of void formation if the concentric pattern is used.



Fig. 5. Biaxial testing machine clamps and an ABS cylindrical specimen.

sample of three specimens was printed to half-height. The outer (D_o) and inner (D_i) diameters of these specimens were then measured using a digital caliper, and the resulting averages (Table S3) were utilized for all subsequent mechanical calculations.

4. Results and discussion

4.1. Uniaxial tensile tests

Fig. 6 displays the representative stress-strain curves from uniaxial tensile tests on ISO 527 1BA specimens, and Table 3 summarizes the calculated mechanical properties: elastic modulus (E), Poisson's ratio (ν), shear modulus (G), tensile strength (σ_{Max}), and strain at break (ϵ_{Max}).

All tested ABS specimens exhibited the typical behaviour of brittle polymers [33] ultimately failing due to interlayer fracture. The measured elastic modulus, strength, and strain at break were significantly lower than both the manufacturer's datasheet values (Table 2) and typical bulk ABS [34]. This performance gap suggests that the manufacturer likely evaluated flat-printed specimens, whereas the vertical orientation used in this study is known to yield suboptimal mechanical properties [35–43]. The reduced strength of these vertical

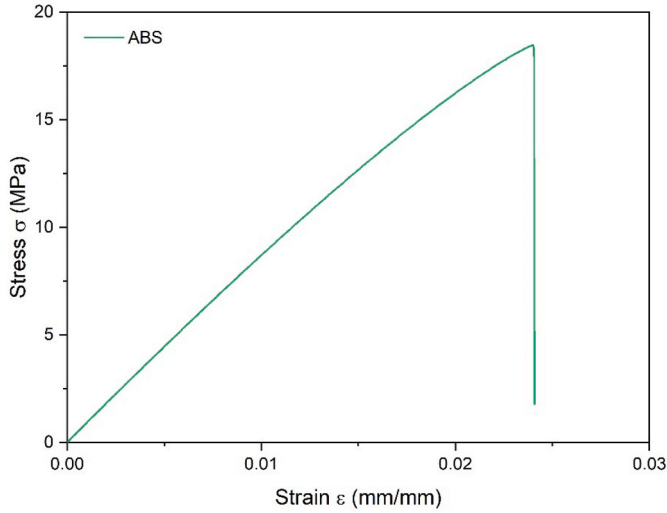


Fig. 6. Representative stress - strain curves from uniaxial tensile tests on ISO 527 1BA ABS specimens.

Table 3

Mechanical properties of Bambu Lab® Green ABS obtained from uniaxial tensile tests on ISO 527 1BA specimens.

Material	E [MPa]	ν	G [MPa]	σ_{Max} [MPa]	ϵ_{Max} [%]
ABS	1731 ± 16	0.404 ± 0.026	616 ± 22	18.3 ± 0.6	2.5 ± 0.1

specimens is driven by high void fractions and poor interlayer adhesion [44]. For context, literature evaluating flat-printed ABS reports significantly higher mechanical performance, achieving ultimate tensile strengths exceeding 25 MPa and elastic moduli above 2300 MPa [45]. This stark contrast further confirms the severe impact that the vertical build orientation and extended inter-layer cooling times have on the macroscopic strength of the specimens tested in this study. Specifically, printing ten specimens concurrently increased the inter-layer cooling time to an average of 14 s; this delay caused the previously deposited layers to cool significantly, restricting polymer chain diffusion and weakening the subsequent bonds [46–48]. Despite these structural limitations, the calculated Poisson's ratio remains consistent with standard literature values, which generally range from 0.36 [49,50] to 0.4 [43,51,52].

4.2. Axial and torsional tests on cylindrical specimens

Fig. 7 shows the representative stress-strain curves obtained from the mechanical tests on the cylindrical specimens for the three different loading conditions: tension, compression and torsion. In Table 4 are reported the values of apparent elastic and shear moduli (E_{app} , G_{app}), yield stress (σ_y , τ_y) and maximum stress (σ_{Max} , τ_{Max}).

Notably, the elastic and shear moduli are significantly lower than those obtained from the uniaxial tensile tests (Table 3). This discrepancy is primarily due to the compliance of the testing machine, which introduces extraneous displacement into the measurements. Consequently, the linear elastic region appears less steep, leading to lower apparent elastic and shear moduli. The correction of these values to account for machine compliance is discussed in Section 4.2.1.

The material can sustain compressive stresses nearly 2.5 times greater than those under tension or torsion. The compression trends observed here share characteristics with typical bulk ABS [53–55], transitioning from a linear elastic region to a non-linear plateau, but diverge in the post-plateau phase. While the upward stress trend in bulk ABS is typically caused by lateral bulging and an increasing

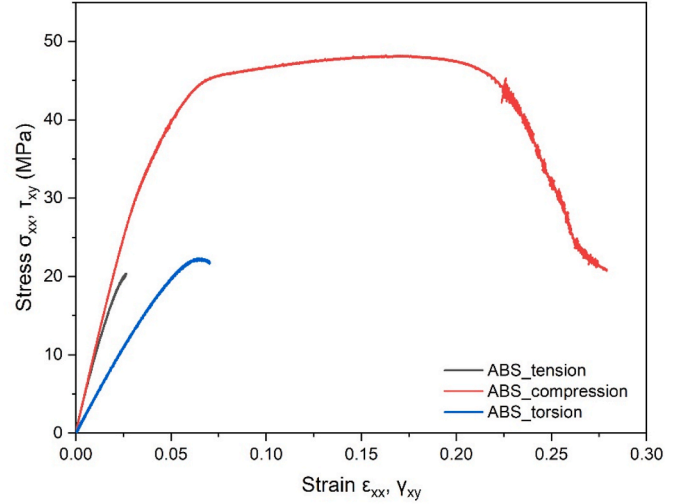


Fig. 7. Representative stress - strain curves of cylindrical specimens for different loading conditions.

Table 4

Apparent elastic and shear moduli, yield stress and maximum stress values resulting from the tests on ABS cylindrical specimens.

Test	E_{app} , G_{app} [MPa]	σ_y , τ_y [MPa]	σ_{Max} , τ_{Max} [MPa]
Tension	1037 ± 43	15.5 ± 0.8	19.6 ± 1.1
Compression	1102 ± 10	24.4 ± 0.6	47.3 ± 1.4
Torsion	474 ± 5	11.6 ± 0.8	22.3 ± 0.1

cross-sectional area, this contribution is minimal in thin-walled tubular specimens. Instead, the rising stress in these FDM parts is likely attributed to material densification. Because of the intrinsic voids formed during the printing process [56,57], the FFF structure behaves similarly to a cellular material that densifies under compression [58] before reaching a maximum stress and undergoing gradual collapse. Similar stress-strain curves were reported by Brischetto et al. [59] for 100% filled cylindrical specimens, although they attributed the observed behaviour to a typical buckling mode occurring when the length-to-width ratio exceeds five. To evaluate the potential for instability in the current study, Johnson's parabolic formula, defined in Equation (7), is utilized to calculate the critical stress ($\sigma_{cr,JO}$). The applicability of this model is determined by comparing the critical slenderness ratio (A_{cr}), defined in Equation (8), with the effective slenderness ratio (λ) from Equation (9).

$$\sigma_{cr} = \sigma_y - \frac{(\sigma_y \lambda)^2}{4\pi^2 E} \quad (7)$$

$$A_{cr} = \sqrt{\frac{2\pi^2 E}{\sigma_y}} \quad (8)$$

$$\lambda = l_e / \rho_g \quad (9)$$

where l_e is the effective length, ρ_g the radius of gyration. If $\lambda < A_{cr}$, the specimen is classified as a “short column”, making the Johnson parabola more appropriate than Euler's formula (Equations S4–S7 [60]). By imposing a fixed-fixed column condition ($l_e = 0.5L$) and utilizing the experimentally derived elastic modulus ($E = 1731$ MPa) and maximum compressive stress ($\sigma_{Max,comp} = 47.3$ MPa), the effective slenderness ratio (λ) is calculated to be 2.3. Because this is significantly lower than the critical slenderness ratio ($\lambda_{cr} = 26.9$), the specimen classifies as a short column, validating the use of Johnson's parabola (Equation (7)) over Euler's global instability theory. Applying Equation (7) yields a global

critical buckling stress of $\sigma_{cr,Johnson} = 47.1$ MPa. Furthermore, evaluating the potential for local instability (wall buckling), the cylindrical shell theory (Equations S1–S3 [61,62]) predicts a critical local buckling stress of $\sigma_{cr,local} = 216$ MPa, much lower than Euler's theory value of $\sigma_{cr,Euler} = 3229$ MPa. Because both the global and local theoretical elastic buckling thresholds far exceed the material's measured ultimate compressive strength, and $\sigma_{cr,Johnson}$ is extremely close to the measured maximum compressive strength of the material, it is confirmed that failure is driven by plastic collapse rather than geometric instability or buckling.

When benchmarked against the existing literature on FFF-printed solid ABS bars, the thin-walled geometry demonstrates significant structural efficiency. For instance, Sadaghian et al. [10] and Ferreira et al. [15] report similar maximum torsional shear stresses of roughly 18 MPa for specimens printed with a $0^\circ/90^\circ$ raster angle or concentric pattern, and 22 MPa for those printed at $\pm 45^\circ$ or with a zigzag pattern. The latter value is comparable to the maximum shear stress found in this study (22.3 MPa); thus, the thin-walled tubular design matches the peak resistance of solid specimens printed with optimal raster orientations. Regarding the failure mechanism, the stress-strain curves reported by Sadaghian et al. exhibited a yield point followed by a plastic plateau, which contrasts with the brittle behaviour observed in our tubular design. This ductile response in solid bars occurs because the inner layers are subjected to lower stresses than the outer layers, allowing the specimen to undergo larger deformations before complete rupture. Conversely, Ferreira et al. observed a more brittle behaviour, which aligns closely with our findings. Ultimately, these comparisons underscore the advantage of the thin-walled tubular design: it maximizes the polar moment of inertia and ensures a highly uniform shear stress distribution across the thickness, effectively mitigating the premature failure often initiated at the core of solid 3D-printed cylinders.

4.2.1. Elastic properties corrections

Because of the large diameter of the specimens, needed to obtain sufficiently high signal to noise ratio, it was not possible to mount an extensometer therefore, to accurately determine the true elastic and shear moduli of the specimens, the apparent axial and angular displacements recorded by the crosshead were corrected for the load-train compliance. Two main methods exist to calculate the compliance of the system:

- Rigid (or dummy) specimen method
- Multiple-length method (also called zero-length extrapolation method), typically used in fiber testing and mathematically described in the ASTM C1557 standard [63].

While non-contact optical techniques like digital image correlation (DIC) are powerful alternatives for strain measurement, these systems are quite expensive and difficult to set up, especially if the geometry of the tested specimen and the applied stress field become quite complex, as planned for future work. Furthermore, DIC captures highly localized strain fields, which can be disproportionately affected by local void concentrations typical of FFF, and is unable to capture the full picture if the tested component becomes significantly large. The following compliance correction methods provide a fast, flexible, and cost-effective homogenized evaluation of the macroscopic modulus without requiring complex optical setups.

4.2.1.1. Rigid specimen method (RS). The rigid specimen method is fast and easy to perform and revolves around the idea of using an extremely rigid sample (like a thick cylinder of hardened steel) and perform the desired test (axial or torsional). Since it is assumed that the specimen does not deform being much stiffer than the machine, the obtained deformation (therefore the compliance) is only related to the machine. The problem with this method is that it does not allow to consider the deformation of the grip zones of the specimen therefore in this case is not

able to capture a realistic picture of the problem.

Fig. 8 reports the tensile, compression and torsion test performed on a single rigid specimen (steel cylinder with a diameter of 2 cm). The data were fitted to obtain the stiffness of the material. Fig. 9 shows an example of the correction process of a tensile test on a cylindrical specimen. Table 5 reports the intercept (q) and the slope (m) of the fitting line, the apparent and RS corrected values of elastic and shear moduli using the rigid specimen method (E_{SB} and G_{SB}) and the ones obtained from the uniaxial tensile tests on ISO 527 1BA specimens using the extensometer (E_{ext} and G_{ext}). The corrected values were obtained by adjusting E_{app} and G_{app} for each specimen, followed by the calculation of the average and standard deviation.

As it can be noticed, this method is not able to capture the full picture of the problem and does not allow to compensate for the deformation of the grip zones of the specimens, leading to a minor correction.

4.2.1.2. Multiple – length method (ML). The multiple-length method instead, allows for the isolation of the deformation occurring solely within the gauge length. For the method to be valid, the specimens must have varying gauge lengths (L_0), but the gripping and transition zones must be geometrically identical across all specimens. When the machine crosshead moves, the total recorded displacement includes several sources of compliance:

- C_m : Compliance of the machine frame and the load cell.
- C_{grip} : Compliance of the grips and micro-slippages.
- $C_{transition}$: Compliance of the transition zones (the fillets where the diameter changes from the grip head to the gauge length).
- C_0 : Compliance of the specimen.

The equation for the total compliance (C_{tot}) is therefore:

$$C_{tot} = C_0 + C_{transition} + C_{grip} + C_m \quad (10)$$

The compliance of the specimen depends on the variable length L_0 :

$$C_0 = \frac{L_0}{E \cdot A} \quad (11)$$

Substituting C_0 into the total equation and grouping all terms that remain constant from one specimen to another:

$$C_{tot} = \left(\frac{1}{E \cdot A} \right) \cdot L_0 + \underbrace{C_{parasitic}}_{\text{Intercept (q)}} \quad (12)$$

Slope (m)

Where $C_{parasitic}$ is a constant (the intercept):

$$C_{parasitic} = (C_{transition} + C_{grip} + C_m) \quad (13)$$

By testing specimens with varying gauge lengths, L_0 , (here selected as 30, 40 and 50 mm) at the three different loading conditions, it is now possible, as shown in Fig. 10, to plot a graph where the x-axis represents the gauge length (L_0) and the y-axis represents the measured total compliance (C_{tot}). One specimen was tested for each gauge length and loading condition, for a total of nine specimens. The experimental points are then fitted linearly. The intercept (q), which is the point where the regression line intersects the y-axis, no longer represents solely the machine compliance; rather, it is a “total parasitic compliance” that encompasses the machine, the grips, and the entire deformation of the transition zones. All “disturbance” effects (including the transition zone) have been confined to the intercept and do not affect the slope (m) of the line.

The slope of the curve (m) is determined exclusively from the true elastic modulus and from the cross-sectional area since all the additional effects have been relegated into the intercept (q). To obtain the true elastic modulus of the tested specimens it is sufficient to rearrange the equation of the slope, m:

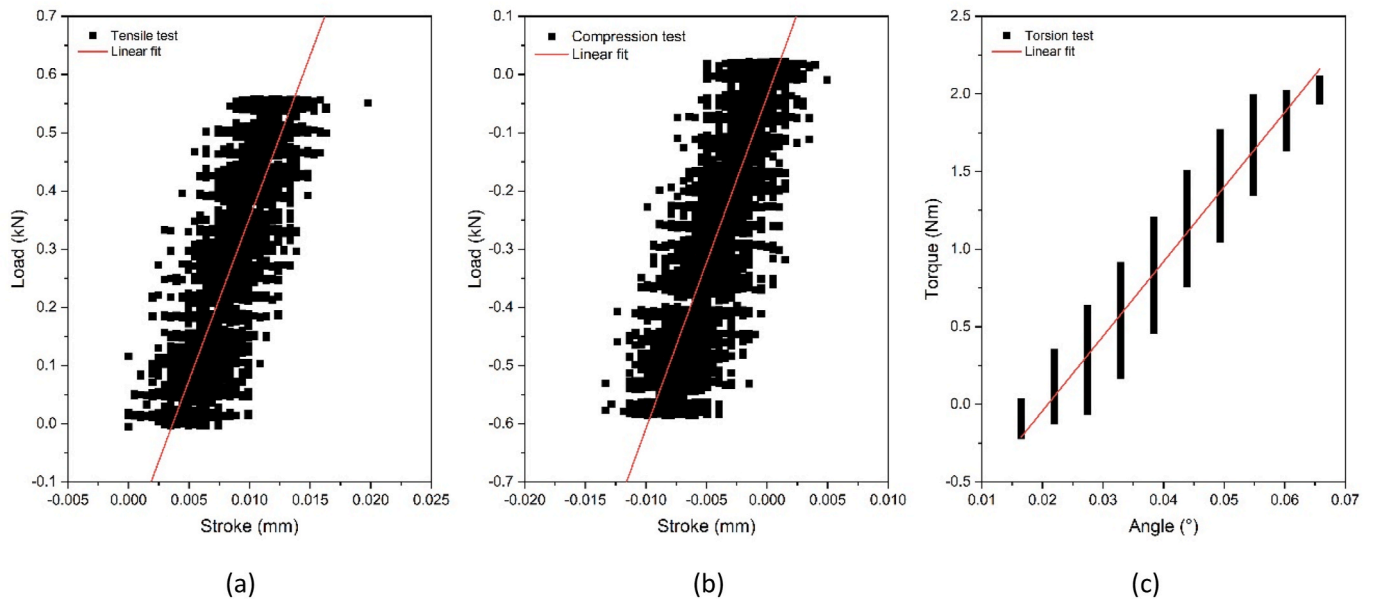


Fig. 8. Rigid specimen method test curves and corresponding linear fit. (a) Tensile test, (b) compression test, (c) torsion test.

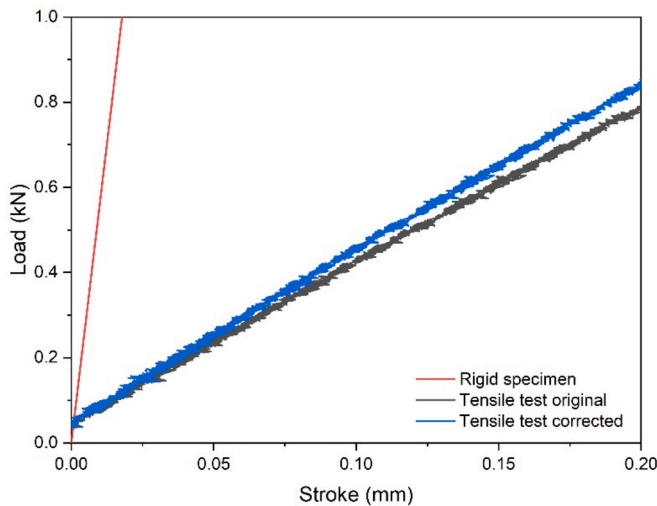


Fig. 9. Example of elastic modulus correction through the RS method. Load – stroke curves of the rigid specimen (red), the tensile test on cylindrical specimen (black) and the corrected curve with the rigid specimen method (blue).

$$E_{\text{true}} = \frac{1}{m \cdot A} \quad (14)$$

Once the tests with multiple lengths are performed and the intercept ($C_{\text{parasitic}}$) is obtained from the graph, to correct the values of other tests not used to produce the linear fit, $C_{\text{parasitic}}$ simply needs to be subtracted from the calculated C_{tot} of each previous measurement using the relative apparent modulus, E_{app} as indicated in Equation (15). In this way, the ML corrected compliance (C_{ML}) of the gauge length alone is derived by

subtracting the previously calculated parasitic compliance from the total compliance:

$$C_{\text{tot}} = \frac{L_0}{E_{\text{app}} \cdot A} \quad (15)$$

$$C_{\text{ML}} = C_{\text{tot}} - C_{\text{parasitic}} \quad (16)$$

The ML corrected elastic modulus can be then calculated by rearranging the equation:

$$E_{\text{ML}} = \frac{L_0}{C_{\text{ML}} \cdot A} \quad (17)$$

A similar approach can be used to derive the ML corrected shear modulus, G_{ML} .

Table 6 reports the intercept (q), the slope (m), the apparent elastic and shear moduli obtained from the tests (E_{app} and G_{app}), the ML corrected values (E_{ML} and G_{ML}) and the ones obtained from the uniaxial tensile tests on ISO 527 1BA specimens using the extensometer (E_{ext} and G_{ext}). The corrected values calculated through the multiple length method are much closer to the measured ones compared to those obtained with the rigid specimen method.

It is noteworthy that the ML-corrected shear modulus ($G_{\text{ML}} = 719$ MPa) deviates from the extensometer-derived value ($G_{\text{ext}} = 616$ MPa). This discrepancy is physically justified by the material's anisotropy. The G_{ext} value was derived mathematically using the isotropic assumption $G = E/[2(1 + \nu)]$ of Equation (2). However, FFF parts are highly anisotropic, meaning the isotropic relationship does not strictly hold. The G_{ML} value represents the true, directly measured macroscopic shear response of the printed layers under pure torsion, highlighting the limitation of using isotropic derivations for FFF materials.

In terms of elastic properties, Sadaghian et al. [10] reported a shear modulus of approximately 350–400 MPa for solid 3D-printed specimens. In contrast, the present study measured an ML-corrected shear modulus

Table 5

Apparent and RS corrected values of elastic and shear moduli using the rigid specimen method for a pure tensile, compression and torsion test.

Test	q [mm/N]	m [N^{-1} , ($\text{N} \cdot \text{mm}^2$) $^{-1}$]	E_{app} , G_{app} [MPa]	E_{SB} , G_{SB} [MPa]	E_{ext} , G_{ext} [MPa]
Tensile	−0.2036	55.78	1037 ± 43	1112 ± 50	1731 ± 16
Compression	−0.0374	57.05	1102 ± 10	1185 ± 12	1731 ± 16
Torsion	−1.0033	48.09	474 ± 5	497 ± 5	616 ± 22

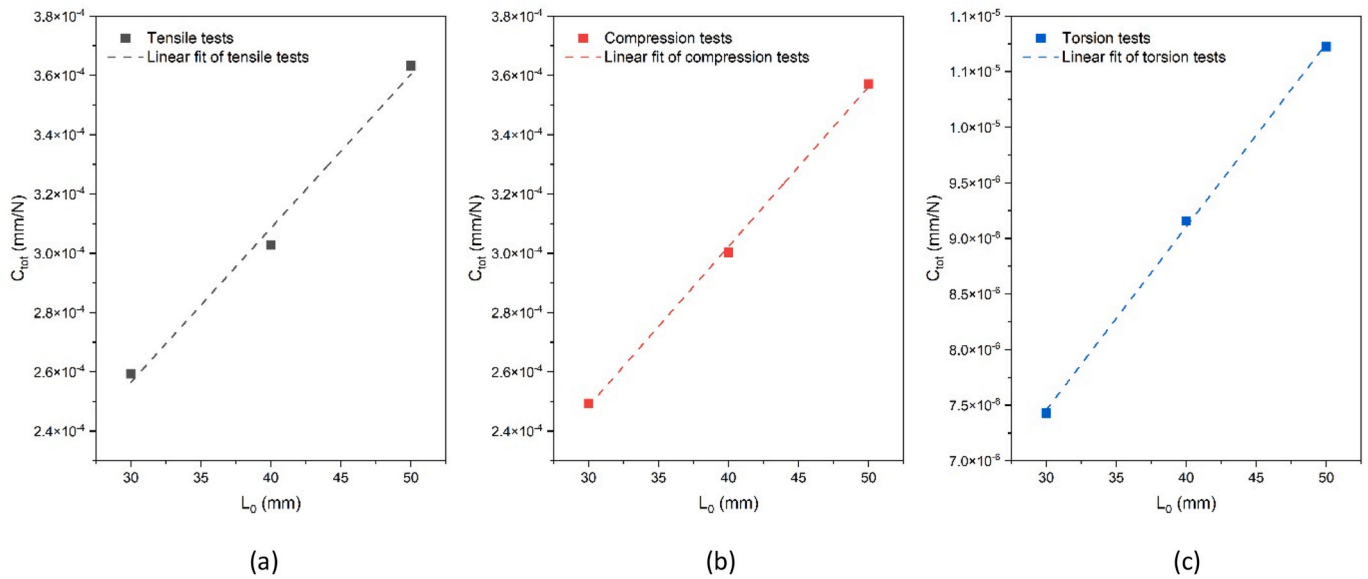


Fig. 10. Total compliance of the specimens as a function of specimens' gauge length (L_0) and corresponding linear fit. (a) Tensile test, (b) compression test, (c) torsion test.

Table 6

Intercept (q), slope (m), apparent elastic and shear moduli (E_{app} and G_{app}), ML corrected values (E_{ML} and G_{ML}), uniaxial tensile tests values using the extensometer (E_{ext} and G_{ext}).

Test	q [mm/N]	m [N^{-1} , ($N \cdot mm^2$) $^{-1}$]	E_{app} , G_{app} [MPa]	E_{ML} , G_{ML} [MPa]	E_{ext} , G_{ext} [MPa]
Tensile	1.01E-04	5.19E-06	1037 $\pm$ 43	1666 $\pm$ 111	1731 $\pm$ 16
Compression	8.68E-05	5.39E-06	1102 $\pm$ 10	1685 $\pm$ 24	1731 $\pm$ 16
Torsion	2.51E-06	1.65E-07	474 $\pm$ 5	719 $\pm$ 10	616 $\pm$ 22

of 719 MPa. This significant difference further confirms that the thin-walled design not only maximizes ultimate shear resistance but also greatly improves the macroscopic torsional stiffness of the system. By eliminating the low-stress inner core found in solid bars, the tubular geometry ensures that all deposited material actively bears the torsional load.

4.3. Limitations and future work

A limitation of the current study is the restriction to a single material (ABS), a single build orientation (vertical), and a single infill configuration (100% concentric). Because the mechanical properties of FFF parts are highly governed by printing anisotropy, the specific elastic constants and ultimate strengths reported here are inherently tied to these parameters. However, it should be noted that the main primary objective of this work was the design and the structural validation of the specimen geometry, alongside the establishment of the compliance correction method. Building upon this validated foundation, ongoing subsequent studies will systematically investigate the multiaxial mechanical behaviour across different 3D-printed materials and varying raster orientations.

5. Conclusions

A cylindrical, thin-walled specimen was successfully designed to maximize torsional resistance and test FFF 3D-printed ABS under various complex loads. By modifying the clamping zones to feature a reduced radius, 100% infill, and longitudinal grooves, the design effectively managed high gripping pressures and eliminated slippage during axial and torsional loading.

The proposed geometry overcomes the heterogeneous stress

distributions inherent to solid bars utilized in previous studies. Furthermore, it embodies the core advantages of 3D printing by minimizing material waste and enabling optimized design, all while facilitating the complex multiaxial loads typical of structural applications.

Mechanical testing showed that these cylindrical ABS specimens sustain compressive stresses of about 47.3 MPa, nearly 2.5 times greater than those observed under tension (19.6 MPa) or torsion (22.3 MPa). Analytical validation using Johnson's parabolic formula and the cylindrical shell theory confirmed that the ultimate failure under compression is driven by plastic collapse and the densification of internal voids, rather than structural buckling.

Because standard extensometers could not be used, correcting for apparent displacements recorded by the crosshead was critical. The study proved that the multiple-length method accurately isolates the deformation within the gauge length by confining transition and machine compliance into a calculated "parasitic compliance" intercept. This approach yielded corrected elastic and shear moduli that were significantly more accurate than those estimated using the traditional rigid specimen method.

While advanced non-contact techniques like digital image correlation (DIC) offer excellent localized strain mapping, they can be costly and difficult to implement on the inherently ridged and/or complex surfaces of FFF parts. The ML compliance correction method presented here provides a robust, highly accessible, and low-cost alternative to accurately determine macroscopic multiaxial properties in 3D-printed polymers.

CRediT authorship contribution statement

Mirko Coser: Writing – original draft, Software, Methodology, Investigation, Data curation. **Luca Fambri:** Writing – review & editing,

Supervision. **Alessandro Pegoretti**: Writing – review & editing, Supervision.

Declaration of competing interests

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

Appendix A. Supplementary data

Supplementary data to this article can be found online at <https://doi.org/10.1016/j.polymertesting.2026.109336>.

Data availability

Data will be made available on request.

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