



# Design, modeling, and optimization of a Frustrated Total Internal Reflection (FTIR)-based tactile force sensor

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## ABSTRACT

The design, modeling, and experimental evaluation of a tactile force sensor utilizing the principle of Frustrated Total Internal Reflection (FTIR) is presented. The sensor device consists of a waveguide, a silicone probe, a light detector, and an LED source. The study begins with a comprehensive material selection process aimed at identifying optimal optical and mechanical properties to ensure efficient light transmission under applied pressure. Hertzian contact theory and a linear-isotropic finite element model are employed to simulate the mechanical interaction between the probe and the waveguide. Finite element method (FEM) simulations are then conducted to compare the numerical and analytical results and to analyze the sensor's static and dynamic responses under various conditions. An optimized configuration featuring three detectors and multiple probe geometries is proposed to achieve a consistent correlation between applied force and optical output. The FEM results are compared with experimental measurements. Additional experimental investigations, including static calibration, hysteresis, repeatability, drift, and dynamic tests, are performed to assess the sensor's stability and reliability under cyclic loading. The findings confirm that the developed FTIR-based tactile sensor exhibits adequate sensitivity, strong repeatability, and stable performance, demonstrating its potential for practical tactile sensing applications.

## 1. Introduction

Manual tool manipulation lies at the core of nearly all practical human activities. A key aspect of such manipulation is the acquisition of contact information. During object handling, sensors embedded within devices can capture critical data—including timing, position, and applied forces—thereby providing valuable insights into interaction dynamics [1]. These devices are typically simple, cost-effective, and highly beneficial across multiple research domains, such as occupational health, workload assessment, and human-machine interaction [2].

Before the twenty-first century, the ability to measure forces and pressures exerted by human hands was limited across many applications. Modern tactile sensing systems have largely addressed these limitations. To evaluate hand forces during various activities, researchers utilize diverse tools, including tactile sensors. Multiple techniques exist for tactile sensing to measure contact location, pressure, force, or strain, such as capacitive, resistive, piezoresistive, piezoelectric, optical, and magnetic methods, each operating on a unique physical principle [3]. Resistive tactile sensors typically consist of conductive layers separated by a porous medium, with electrodes at the base to detect voltage changes. When a DC current is applied to the top

layer, contact causes variations in resistance that can be measured. For instance, a study [4] introduced a 3D-printed Electrical Impedance Tomography (EIT) pressure sensor for wearable robot interfaces. By compensating for anisotropic conductivity in 3D-printed parts using an adjusted Jacobian matrix, the sensor achieved real-time monitoring of pressure distribution on customized forearm interfaces, validated with a rehabilitation cobot. Another example is a flexible resistive sensor integrating  $\text{Ti}_3\text{C}_2\text{T}_x$  MXene nanosheets and Fe nanowires into a polyurethane sponge, forming a 3D conductive network. This device exhibited high sensitivity ( $6.9\text{ kPa}^{-1}$ ), fast response, durability, and dual functionality, including spatial pressure mapping and Joule heating, making it suitable for wearable healthcare and robotics applications [5]. Unlike conventional resistive sensors that only detect contact points, piezoresistive sensors can quantify both the location and magnitude of strain, as resistance changes proportionally with applied force. Wu et al. [6] developed a flexible EIT-based tactile sensor with a double-sensitive-layer structure, enabling multi-point pressure mapping and effective detection of both contact position and force, suitable for human-robot interaction. Piezoelectric tactile sensors rely on the piezoelectric effect, generating electrical charges proportional to applied force, making them ideal for capturing dynamic events such

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as vibration and impacts. Rasouli et al. [7] designed a neuromorphic tactile system using a piezoresistive fabric sensor coupled with an Extreme Learning Machine (ELM) chip for texture recognition. The system achieved 92 % accuracy across ten graded textures, demonstrating efficient and energy-efficient tactile pattern recognition. Capacitive tactile sensors measure changes in capacitance caused by strain in a compressible dielectric layer, such as microstructured rubbers or nanowire arrays, placed between electrodes. Chen et al. [8] developed a capacitive sensor with a gradient porous PDMS/MWCNT composite dielectric, enhancing sensitivity ( $0.134 \text{ kPa}^{-1}$ ), extending linear sensing range (0–200 kPa), and maintaining durability over 3000 cycles, enabling applications in pulse detection, gait monitoring, and wearable human–machine interfaces. Optical tactile sensors often employ fiber Bragg grating (FBG) technology, where an optical fiber attached to the surface detects shifts in reflected light caused by deformation. Lee et al. [9] introduced a multi-layered vision-based tactile sensor (ML-VBTS) combining elastomers of different hardness to overcome the sensitivity–range trade-off. Validated through finite element analysis and Sim2Real testing, it achieved approximately 60 % improvement over the GelSight mini, enhancing detection of normal and shear forces for advanced robotic applications. Magnetic tactile sensors detect strain via variations in a magnetic field using devices such as Hall-effect or GMR sensors. Early designs embedded permanent magnets in soft substrates like silicone, while newer approaches use magnetic fillers for greater flexibility and reduced size. Jamone et al. [10] developed a highly sensitive soft tactile sensor for anthropomorphic robotic hands, embedding a magnet in a silicone body with a Hall-effect sensor to measure pressure-induced magnetic changes. This design provided high sensitivity, low hysteresis, and repeatability, facilitating effective human–robot interaction.

Frustrated Total Internal Reflection (FTIR) is a widely used phenomenon in optical sensors. This technique directs infrared (IR) light into a transparent medium, where it undergoes internal reflection. When an object, such as a fingertip, contacts the surface, it disrupts the reflection and scatters the light, which is then detected by sensors placed beneath or beside the medium [11]. Wattanapartint et al. [12] proposed a vision-based tactile sensor that employs re-propagated FTIR to produce multiple contact images, validating the concept through simulations and a prototype that estimates contact positions using regression algorithms. Their experiments demonstrated accurate contact estimation. Wattanapartint et al. [13] developed RFTIRTouch, a device that captures re-propagated FTIR images from a dual-sided transparent plane, estimating touch positions with a physics-based method. The device can be retrofitted onto existing transparent media, achieving a touch-position error of approximately 2.1 mm and enabling dual-sided, waterproof sensing. Additionally, an optical-electronic sensor [14] has been designed for rain and fog detection based on FTIR, offering a simple, robust, and sensitive solution suitable for aerospace applications. The study reviews various sensor types and identifies optimal designs. Ghaffar et al. [15,16] proposed force and pressure sensors based on twisted polymer optical fibers (POFs) utilizing the cladding-mode frustrated total internal reflection (CMFTIR) effect. The studies investigated various fiber configurations, including straight, twisted, twisted-bend, and twisted-helical structures, and demonstrated that the twisted-bend configuration provides enhanced sensitivity. Furthermore, the sensors were shown to be capable of multipoint and multi-axis force sensing as well as high-pressure measurements up to 4 MPa. These results highlight the potential of twisted POF-based sensors for a wide range of sensing applications. Beyond these applications, FTIR has also been used in laser beam power control [17], footwear traction testing [18], tire normal contact pressure measurement [19], and other fields. However, existing FTIR-based tactile sensors are primarily optimized for high-resolution contact imaging, which generally requires bulky optical setups and sophisticated processing pipelines, limiting their applicability in compact force-sensing systems.

This study aims to present a compact FTIR-based tactile force sensor specifically designed for hand manipulation tasks. Unlike conventional FTIR tactile sensors that often rely on camera-based imaging systems and complex optical arrangements for contact reconstruction, the proposed sensor employs a simplified intensity-based optical configuration, where the force-induced deformation of the compliant interface is directly correlated with the variation of transmitted light. The sensor design is developed based on contact mechanics principles and validated through finite element analysis to establish the relationship between applied force and optical response. Experimental investigations further demonstrate the capability of the proposed sensor for reliable force measurement during hand manipulation tasks. The proposed design offers several advantages, including a compact structure, ease of integration with embedded controllers, reliance exclusively on commercially available off-the-shelf components, and elimination of cameras, complex data acquisition systems, and computationally intensive image-processing algorithms. These characteristics make the proposed FTIR tactile sensor a promising solution for low-cost and scalable integration into robotic grippers, human–machine interfaces, and wearable sensing systems.

The paper is structured as follows: Section 2 describes the sensor methodology, including the FTIR phenomenon, sensor configuration, and material selection. In Section 3, Hertzian contact theory is used to model the sensor response, while the results of finite element modeling of the sensing units are presented in Section 4. The following section examines the sensor's design optimization, focusing on shape analysis and detector evaluation (Section 5). Metrological experiments are then presented for validation (Section 6). The paper concludes with a summary of the key findings.

## 2. Sensor methodology

A brief explanation of the physical phenomenon utilized in the sensor is provided, followed by a detailed description of the sensor's components, including the electrical and mechanical elements. Finally, material selection is discussed to ensure optimal performance of the sensor.

### 2.1. Frustrated total internal reflection

Refraction occurs when light passes between media in which it propagates at different speeds. If the refractive index of the first medium ( $n_1$ ) is greater than that of the second ( $n_2$ ), the refracted angle ( $\theta_r$ ) is greater than the incident angle ( $\theta_i$ ) (Fig. 1(a)). As  $\theta_i$  increases to the critical angle  $\theta_c$ , the refracted ray approaches the interface. Above  $\theta_c$ , the energy is completely reflected into the first medium, producing total internal reflection (Fig. 1(b)). Snell's law relates the angles and refractive indices as follows:

$$\frac{\sin(\theta_r)}{\sin(\theta_i)} = \frac{n_1}{n_2}. \quad (1)$$

When light undergoes Total Internal Reflection (TIR), an evanescent wave is generated at the boundary between two media. This wave does not travel into the second medium but instead diminishes exponentially with increasing distance from the interface [20]. However, if a third medium with a higher refractive index than the second is positioned very close to the interface—within the reach of the evanescent field—the wave can be “frustrated” and partially transmitted into this third medium, effectively allowing the light to tunnel through it (Fig. 1(c)). This phenomenon is known as Frustrated Total Internal Reflection (FTIR), in which evanescent waves are fundamental to enabling the energy transfer [21].

In this study, the phenomenon of Frustrated Total Internal Reflection (FTIR) is employed to develop an optical force sensor. The sensor operates by directing light into a cylindrical polycarbonate waveguide, where it becomes confined due to Total Internal Reflection (TIR). When

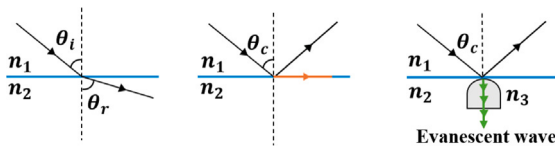


Fig. 1. Optical phenomena: (a) refraction, (b) total internal reflection, and (c) frustrated total internal reflection.

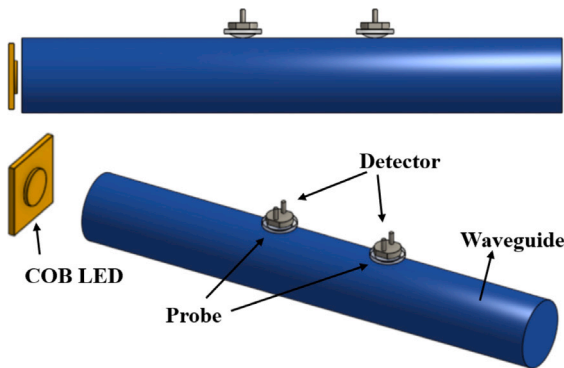


Fig. 2. Components of the OPT-IN sensor concept.

a flexible, transparent probe is brought close to the waveguide, FTIR occurs, allowing some of the light to pass through the probe. As normal force is applied to the probe, the contact area between the probe and the waveguide changes, which in turn alters the amount of light reaching a detector. By measuring this change in light intensity, the applied force can be determined and quantified.

## 2.2. Sensor configuration

The proposed sensor concept, called OPT-IN (OPTical INceptor), is designed to be embedded into the grips of various control devices, such as those used in helicopters, aircraft, drilling machines, tennis rackets, golf clubs, and more. Key advantages of this sensor include low cost, easy adaptability across different applications, and minimal requirements for signal conditioning.

OPT-IN is composed of several main components (Fig. 2). At its core is a polycarbonate cylindrical waveguide illuminated by a COB LED. The sensing element is a transparent hemispherical probe. Positioned behind the probe is a detector—such as a light-dependent resistor (LDR) or photodiode—that captures the amount of transmitted light. The detector is linked to a conditioning electronic circuit and a data acquisition system for signal processing. The modular design allows the sensor to be integrated into devices of various shapes, functioning as a force sensor. The number and placement of probes can be tailored depending on the application and the contact points of interest [22]. See Appendix A for details of the components and circuits.

The applied force from the user directly influences the contact area between the probe and the waveguide. As this contact area increases, the detector records a corresponding increase in light transmission. Consequently, the light intensity received by the detector is proportional to the normal contact force exerted at the interface of the probe and the waveguide. As it will be shown in the following sections, with appropriate material selection, the relationship between applied force and output voltage is nearly linear.

## 2.3. Material selection

Choosing an appropriate material is a critical aspect of product design, as it directly influences device performance and must satisfy

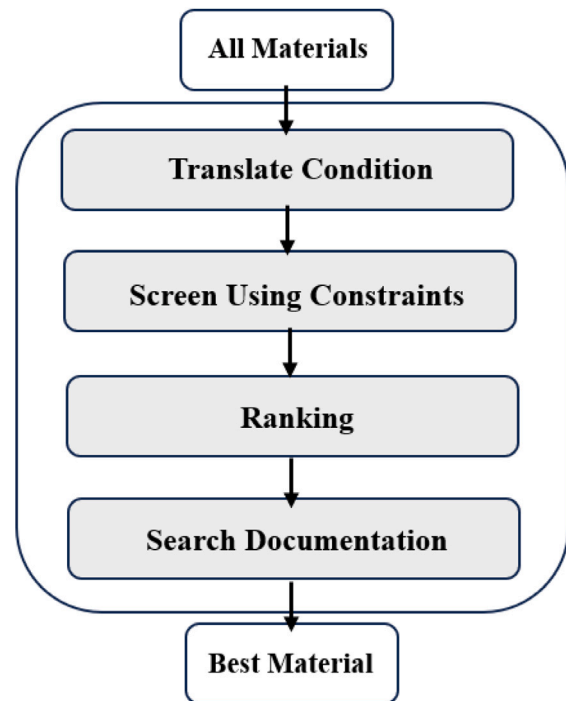


Fig. 3. Material selection strategy.

specific functional requirements. Inappropriate material selection can lead to reduced efficiency, degraded performance, or, in extreme cases, structural or functional failure.

Material selection aims to identify the best compromise among the relevant material attributes required by the application. To achieve this in a systematic and objective manner, a structured selection strategy is necessary. Ashby proposed a well-established methodology for materials selection [23], illustrated in Fig. 3. This approach consists of four main stages: translation, screening, ranking, and documentation. In the translation stage, the functional requirements, constraints, and performance objectives of the design are defined. During screening, materials that do not meet the imposed constraints are eliminated in a “pass/fail” process. The remaining candidates are then compared and prioritized during the ranking phase according to the most relevant performance indices. Finally, the documentation stage involves a detailed assessment of the shortlisted materials, considering their properties, processing routes, and overall suitability for the intended application.

To achieve proper performance in the OPT-IN sensor, the probe material must possess several key characteristics (Translation stage). First, it should be sufficiently transparent to allow light to pass through easily and be detected by the sensor. The contact area, which directly influences light detection, should exhibit good linearity in the relationship between applied load and contact area. In addition, the material should have an appropriate Young’s modulus to allow compression and recovery to its initial shape under the maximum allowed load. The material must be easy to process during manufacturing. It should not display hysteresis effects under vibration and should maintain consistent behavior across temperature variations.

The most suitable candidates for the probe material are elastomers, as they offer low stiffness and high flexibility compared to other material families. Within the elastomer group, rubbers (thermoset elastomers) are selected for this purpose (Screening stage). Since transparency is a critical requirement, elastomers are further classified into transparent, translucent, and opaque categories. Materials that are opaque are excluded, and the remaining suitable options are summarized in Table 1 (Ranking stage).

**Table 1**  
Mechanical and optical properties of transparent rubbers.

| Material                                                                 | Young's modulus (GPa)                         | Refractive index |
|--------------------------------------------------------------------------|-----------------------------------------------|------------------|
| Butadiene rubber (unreinforced)                                          | 0.001–0.0021                                  | 1.51–1.53        |
| Perfluoro elastomer (FFKM)                                               | 0.003 21–0.01                                 | –                |
| Polyurethane rubber (unfilled)                                           | 0.0017–0.0026                                 | 1.51–1.53        |
| Silicone (VMQ, heat cured, low hardness, 5–15 % fumed silica)            | $2.05 \times 10^{-4}$ – $2.15 \times 10^{-4}$ | –                |
| Butyl/halobutyl rubber (IIR, unreinforced)                               | $7 \times 10^{-4}$ –0.0015                    | 1.51–1.52        |
| Ethylene vinyl acetate rubber (EVM, 60–70 % VA)                          | $6.8 \times 10^{-4}$ –0.001 65                | 1.48–1.49        |
| Ethylene acrylic rubber (AEM, unreinforced)                              | $6.8 \times 10^{-4}$ –0.001 65                | 1.60–1.61        |
| Ethylene propylene (diene) (EPDM/EPM, unreinforced)                      | $7 \times 10^{-4}$ –0.0017                    | 1.48–1.49        |
| Acrylic rubber (ACM, unreinforced)                                       | $7.8 \times 10^{-4}$ –0.001 65                | 1.60–1.61        |
| Natural rubber (unreinforced)                                            | 0.0012–0.0021                                 | 1.51–1.53        |
| Epichlorohydrin copolymer (ECO/GECO, unreinforced)                       | 0.0015–0.0021                                 | –                |
| Epichlorohydrin rubber (CO, unreinforced)                                | 0.0015–0.0021                                 | –                |
| Polychloroprene (CR, unreinforced)                                       | 0.001 65–0.0021                               | 1.55–1.57        |
| Nitrile rubber, hydrogenated (HNBR)                                      | 0.0018–0.0021                                 | –                |
| Nitrile rubber (NBR, unreinforced)                                       | 0.0018–0.0021                                 | 1.52             |
| Polyisoprene rubber (unreinforced)                                       | 0.0017–0.0026                                 | 1.51–1.53        |
| Styrene butadiene rubber (SBR, unreinforced)                             | 0.002–0.01                                    | 1.53–1.54        |
| Hard rubber (Ebonite)                                                    | 0.9–2.1                                       | 1.60             |
| Fluorosilicone (FVMQ, heat cured, 10–30 % fumed silica)                  | 0.005–0.05                                    | 1.40–1.44        |
| Silicone (VMQ, heat cured, 10–30 % fumed silica)                         | 0.005–0.05                                    | 1.40–1.44        |
| Silicone, phenyl-type (PVMQ, heat cured, 10–30 % fumed silica)           | 0.005–0.05                                    | 1.40–1.44        |
| Ethylene propylene (diene) (EPDM/EPM, 30–50 % carbon black, plasticized) | 0.002 35–0.0097                               | –                |

Another important factor in light transmission is the refractive index ( $n$ ), a dimensionless quantity that describes how light propagates through a material. If the refractive index of the starting material is  $n_2$ , that of the coating is  $n_1$ , and that of the surrounding medium (usually air) is  $n_0 = 1$ , then the optimum value for the index  $n_1$  of the coating can be calculated according to Eq. (2) [23]:

$$n_1^{\text{opt}} = \sqrt{n_0 n_2}. \quad (2)$$

Therefore, if the waveguide made of polycarbonate has a refractive index of 1.6, the optimal refractive index of the coating material, according to Eq. (2), should be 1.26. This means that, to achieve the best performance in terms of transmitting light through the probe, to the detector, the refractive index of the chosen material should be close to this value.

As illustrated in Table 1, members of the silicone family—including Silicone (VMQ, heat cured, low hardness, 5–15 % fumed silica), Fluorosilicone (FVMQ, heat cured, 10–30 % fumed silica), Silicone (VMQ, heat cured, 10–30 % fumed silica), and phenyl-type Silicone (PVMQ, heat cured, 10–30 % fumed silica)—meet this requirement, exhibiting values around 1.4 (documentation) [24].

Although Eq. (2) predicts an optimal probe refractive index of approximately 1.26, transparent silicone elastomers with refractive index close to this value are not readily available commercially. Therefore, silicone materials with refractive indices of 1.40–1.44 were selected as a practical compromise, providing suitable mechanical and optical properties. Although this refractive-index mismatch may slightly reduce the FTIR coupling efficiency, the experimental results demonstrate sufficient sensor sensitivity, good linearity, and high repeatability. In the following section, silicone is selected as the optimal material for the simulations used to evaluate the static and dynamic responses of the sensor.

### 3. Simplified analytical sensor model

Hertzian contact theory is used to derive an analytical estimate of how the contact area evolves with applied force. Alternative material descriptions were screened for the silicone probe; over the investigated 0–10 N range, the linear-isotropic FEM response, with  $E = 3.5$  MPa and  $\nu = 0.49$ , provided the closest match to the analytical and experimental reference trends and was therefore retained for the analyses reported below.

Hertzian contact theory describes the stress and displacement fields generated when two smooth elastic bodies are pressed together [25,26].

The contact between a cylinder and a sphere is a special case. Before loading, the ideal surfaces touch at a point; an applied normal load deforms the surfaces and produces an approximately elliptical contact region.

For a sphere and a cylinder with aligned principal-curvature directions, the Hertzian pressure distribution is given by Eq. (3); detailed derivations are available in [26].

$$p(x, y) = p_0 \sqrt{1 - \frac{x^2}{a^2} - \frac{y^2}{b^2}} \quad (3)$$

Here,  $a$  and  $b$  are the major and minor semi-axes of the contact ellipse, respectively, and  $p_0$  is the maximum contact pressure. The normal surface displacement is

$$u_z(x, y) = \frac{p_0 b}{E^* a^2 e^2} \left[ a^2 e^2 K(e) - (K(e) - E(e)) x^2 - \left( \frac{E(e)}{1 - e^2} - K(e) \right) y^2 \right] \quad (4)$$

where  $K(e)$  and  $E(e)$  are defined as

$$K(e) = \int_0^{\pi/2} \frac{d\theta}{\sqrt{1 - e^2 \cos^2 \theta}}, \quad (5)$$

$$E(e) = \int_0^{\pi/2} \sqrt{1 - e^2 \cos^2 \theta} d\theta.$$

In (5),  $e$  is the eccentricity of the contact ellipse, and  $\theta$  is the integration variable, with values between 0 and  $\pi/2$ . The total normal contact force is

$$P = 4 \int_0^a \int_0^{b\sqrt{1-x^2/a^2}} p_0 \left( 1 - \frac{x^2}{a^2} - \frac{y^2}{b^2} \right)^{1/2} dy dx \quad (6)$$

$$= \frac{2\pi p_0 ab}{3}.$$

The goal is to find the contact area between two ideal interacting bodies, a cylinder and a sphere. The first step is to compute the eccentricity of the contact ellipse. The geometry of the contacting surfaces is represented by the radii  $R_x^{(1)}$ ,  $R_y^{(1)}$ ,  $R_x^{(2)}$ , and  $R_y^{(2)}$ . The two geometric constants  $A$  and  $B$  can be computed from

$$\frac{1}{R_x} = 2A = \frac{1}{R_x^{(1)}} + \frac{1}{R_x^{(2)}} \quad (7)$$

$$\frac{1}{R_y} = 2B = \frac{1}{R_y^{(1)}} + \frac{1}{R_y^{(2)}}$$

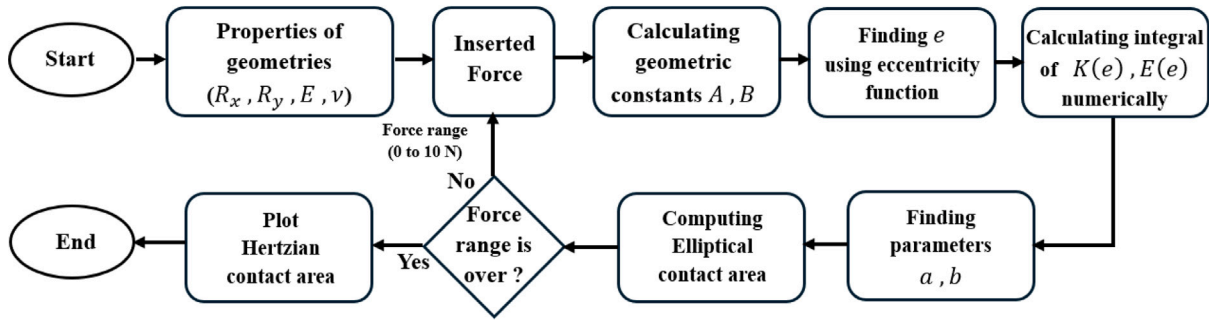


Fig. 4. Flowchart of Hertz contact theory.

Table 2

Properties of the cylinder and sphere.

| Property        | Symbol      | Value    | Unit |
|-----------------|-------------|----------|------|
| Young's modulus | $E_1$       | 3.5      | MPa  |
| Young's modulus | $E_2$       | 2850     | MPa  |
| Poisson's ratio | $\nu_1$     | 0.49     | –    |
| Poisson's ratio | $\nu_2$     | 0.35     | –    |
| Radius          | $R_x^{(1)}$ | 3        | mm   |
| Radius          | $R_y^{(1)}$ | 3        | mm   |
| Radius          | $R_x^{(2)}$ | $\infty$ | mm   |
| Radius          | $R_y^{(2)}$ | 8.5      | mm   |

$$\frac{1}{K(e) - E(e)} \left[ \frac{E(e)}{1 - e^2} - K(e) \right] = \frac{B}{A} \quad (8)$$

The eccentricity, which is the only unknown parameter, can be determined numerically using, for example, the Newton–Raphson method.

When the eccentricity is known, the major and minor semi-axes  $a$  and  $b$  of the elliptical contact area can be determined. The maximum contact pressure can then be computed as  $p_0 = \frac{3P}{2\pi ab}$ :

$$a = \left( \frac{3P[K(e) - E(e)]}{2\pi e^2 E^* A} \right)^{1/3} \quad (9)$$

$$b = a\sqrt{1 - e^2}$$

$$\frac{1}{E^*} = \frac{1 - \nu_1^2}{E_1} + \frac{1 - \nu_2^2}{E_2}$$

Given the applied force range ( $P$ ), the elliptical contact area can be calculated (Fig. 4 shows the calculation procedure). The details related to the contacting surfaces are defined in Table 2, and Fig. 5 shows the force–contact-area response. The curve is nonlinear in the initial 0–2 N range and approximately linear at higher loads.

#### 4. Finite element analysis

A Finite Element Model has been developed and applied across various modules of ANSYS software to simulate the behavior of the OPT-IN components under different loading conditions. First, the model is analyzed in the structural analysis module to obtain its static response. Next, it is employed in the transient structural module to determine the transient response. Finally, the model's Frequency Response Function (FRF) is computed based on its responses to various load conditions. The results from each stage provide valuable insight into static, and dynamic performance of the sensing element, enabling a deeper understanding of its operational limits and potential failure modes. This integrated simulation approach ensures that both short-term and long-term behaviors are accurately captured, forming a robust basis for design optimization and performance improvement.

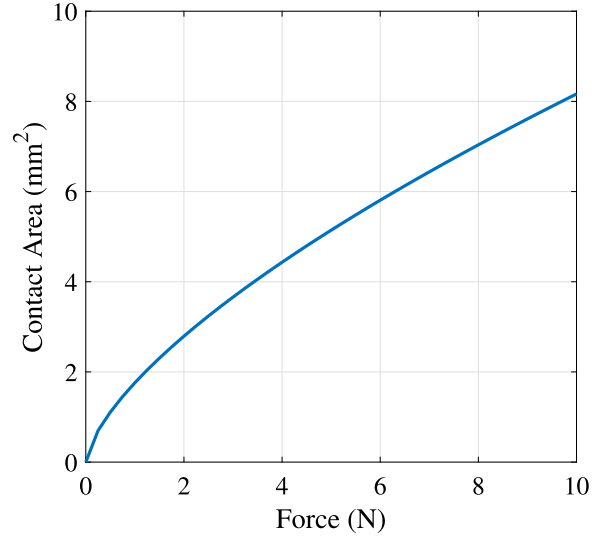


Fig. 5. Hertzian contact area vs. applied force for a cylinder and a sphere with the properties defined in Table 2.

##### 4.1. Static and transient response analysis

In the Finite Element Method (FEM), structural analyses are generally classified into linear and nonlinear types. Nonlinear behavior can arise from several factors, including large deformations, elastic–plastic material properties, and, importantly, contact between components in the geometry. Contact nonlinearity is influenced by parameters such as material properties, friction coefficient, geometric configuration, and applied loading conditions [27]. In the case of the sensor under study, system performance is highly dependent on the contact between the probe and the waveguide; therefore, analyzing the contact area is critical for ensuring measurement accuracy.

In the Static Structural module of ANSYS Workbench, a three-dimensional finite element model was developed, as shown in Fig. 6a. In this model, the waveguide is represented as a half-cylinder made of polycarbonate to reduce the number of elements and accelerate computation. The waveguide is fixed at its bottom. A probe, tangent to the waveguide, is supported by a Base component fabricated from Formlabs Tough 2000™ [28] Resin ( $E = 2.8$  GPa,  $\nu = 0.35$ ). A compressive force is applied to the Base, pressing the probe against the waveguide. The material properties of the cylinder and sphere are listed in Table 2. The applied load ranges from 0–10 N.

Proper meshing plays a vital role in accurately solving contact problems. In this case, the cylinder and sphere are initially not in contact, and interaction occurs only when the probe is pressed against the waveguide. Fig. 6b illustrates the mesh configuration selected for the model components. For the meshing process, the SOLID186

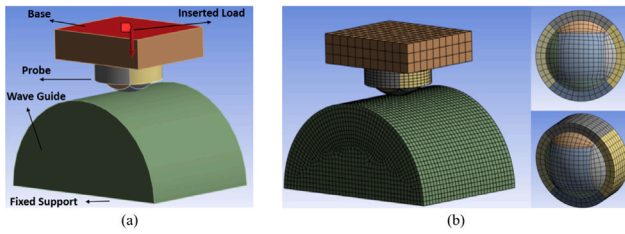


Fig. 6. (a) Geometry generated for analysis (b) Mesh applied to model components.

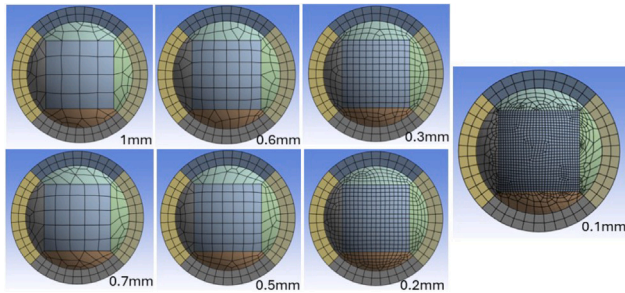


Fig. 7. Different element size for spherical part of the probe.

element type was selected. This is a 3D hexahedral element with 20 nodes and quadratic displacement interpolation. Although it ensures high accuracy, it is also computationally demanding [29]. In addition, the software employs other specialized elements such as CONTA174, TARGET170, SURF154, and COMBIN14 to define the contact interfaces. It is essential that the elements and nodes of the contact and target surfaces align properly to ensure correct interaction. For this reason, an asymmetric contact behavior is adopted, in which nodes on the contact surface are prevented from penetrating the target surface. This approach is generally the most efficient for modeling face-to-face contact between solid bodies. Among the available contact algorithms, the Augmented Lagrangian method is employed for the analysis. This method combines the advantages of the penalty approach and Lagrange multipliers, iteratively adjusting contact forces to maintain constraint accuracy while ensuring numerical stability. It effectively prevents penetration between surfaces while allowing separation or sliding when physically appropriate [30]. For contact detection, Gauss integration points are used, enabling precise evaluation of normal gaps and, when applicable, tangential slip at multiple locations within each contact element. The contact area was evaluated using the contact area result available in ANSYS Mechanical, which determines the active contact region at the interface based on the contact status obtained from the contact formulation. Moreover, a mesh convergence study is performed to verify that the simulation results are independent of the mesh density. For spherical part of the probe, the Hex Dominant meshing method is employed. Within this method, the Free Face Mesh Type option is set to All Quad to improve the mesh quality on the spherical surface. Simulations are carried out using different mesh element sizes, as illustrated in Fig. 7. Element sizes of 1.0, 0.7, 0.6, 0.5, 0.3, 0.2 and 0.1 mm are specified using the Sizing option in ANSYS Meshing. The corresponding numbers of elements and nodes for each mesh are summarized in Table 3. The predicted contact areas obtained with the different mesh sizes are presented in Fig. 8. The results indicate that the calculated contact areas are very similar for all mesh densities, with deviations of less than 5 %, confirming that the numerical solution is effectively mesh-independent for this parameter. It is also observed, as expected, that reducing the element size produces a smoother contact area curve.

The retained FEM represents the silicone probe as a nearly incompressible, linear-isotropic material with  $E = 3.5 \text{ MPa}$  and  $\nu =$

Table 3

Finite element mesh characteristics used in the mesh convergence study.

| Element size (mm) | Number of elements | Number of nodes |
|-------------------|--------------------|-----------------|
| 1.0               | 3316               | 13 519          |
| 0.7               | 3322               | 13 568          |
| 0.6               | 3075               | 13 240          |
| 0.5               | 3154               | 13 478          |
| 0.3               | 3290               | 14 130          |
| 0.2               | 4647               | 18 223          |
| 0.1               | 13 365             | 44 354          |

0.49. The Hertz solution assumes homogeneous linear elasticity, small deformation, frictionless normal contact, and a contact region that is small relative to the bodies' characteristic dimensions [31,32]. The FEM retains the evolving contact geometry. Fig. 9 shows the contact area between the probe and waveguide and how these shapes deform against each other. Fig. 10 shows the calculated static contact area and its comparison with the analytical solution based on Hertzian contact theory.

From the static responses (Fig. 10), it can be observed that all curves exhibit initial nonlinearity in the low-load region (0–2 N), after which they become approximately linear. The FEM with isotropic elasticity shows very good agreement with the analytical Hertzian solution, confirming both the accuracy of the finite element approach and the suitability of the chosen elements for the simulation.

According to Johnson [33], Hertzian theory requires the contact dimension  $a$  to be much smaller than the minimum radius of curvature  $R$ . Here,  $a/R$  reaches approximately 0.6 at 10 N, so this assumption is not strictly satisfied. Nevertheless, the linear-isotropic FEM and Hertzian results agree closely in this specific case. The Hertz model can therefore provide useful early-design estimates, even though our general recommendation is to confirm its prediction by higher-fidelity FEM or direct experimental contact-area measurements, where available.

In the next step, the transient response is evaluated using the transient structural module of ANSYS Workbench. A sinusoidal load with varying frequencies ranging from 0.5 Hz to 10 Hz, with 1 Hz spacing, is applied to the system, and the corresponding contact area responses versus force (transient responses) are recorded. The results corresponding to excitation frequencies of 3.5 Hz and 7.5 Hz are presented in Fig. 11. The two frequencies were selected as representative of the complete dataset, which is characterized by a constant amplitude ratio and minimum phase lag, as evidenced by Fig. 12. As illustrated in Fig. 11, the contact area response does not exhibit the same symmetry as the applied force signal. This asymmetry arises from the progressive engagement of the contact interface as the probe is compressed, even though the retained constitutive law is linear isotropic. The transient response results are used in the following subsections for further analysis.

#### 4.2. Frequency response analysis

The frequency response function (FRF) between the applied force and contact area is calculated from the amplitude and phase of the transient responses at each excitation frequency. The resulting magnitude and phase are reported in Fig. 12.

Fig. 12 shows that the FRF, including both amplitude ratio and phase response, remains flat over the frequency range of 0–10 Hz. The amplitude ratio stays nearly constant at around 0.7, indicating that the system does not display any noticeable resonance within this interval. The absence of peaks in the curve suggests stable dynamic behavior and effective damping. Similarly, the phase response remains close to  $0^\circ$  across the frequency range, reflecting minimal delay between input and output signals. Overall, this response indicates that the system behaves much like a proportional element within the studied frequency band. The lack of sudden variations highlights its linearity and predictability. Such a flat trend confirms robustness against frequency-dependent effects, demonstrating stable and reliable dynamic performance under the test conditions.

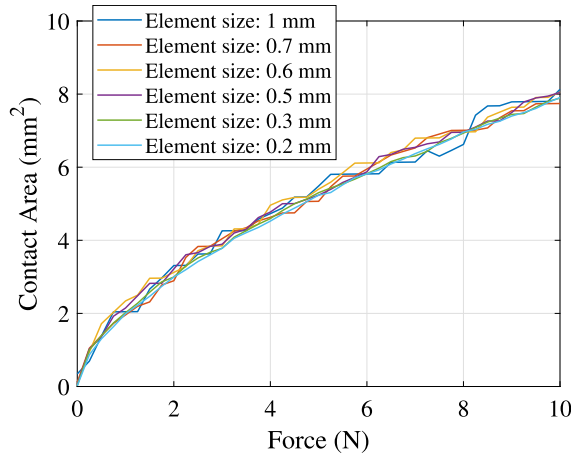


Fig. 8. Contact area vs. Force for different element size.

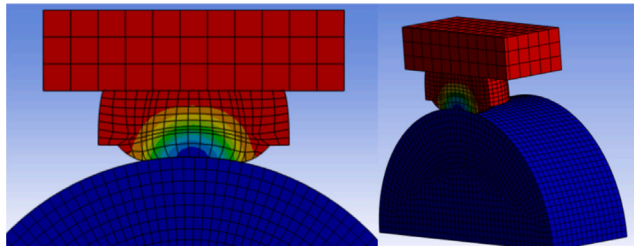
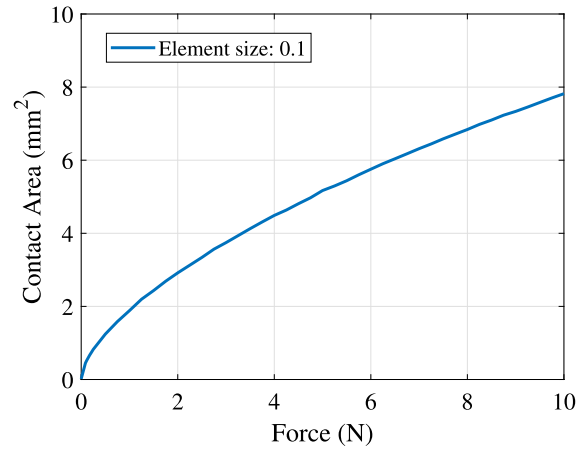


Fig. 9. Contact area between the probe and waveguide.

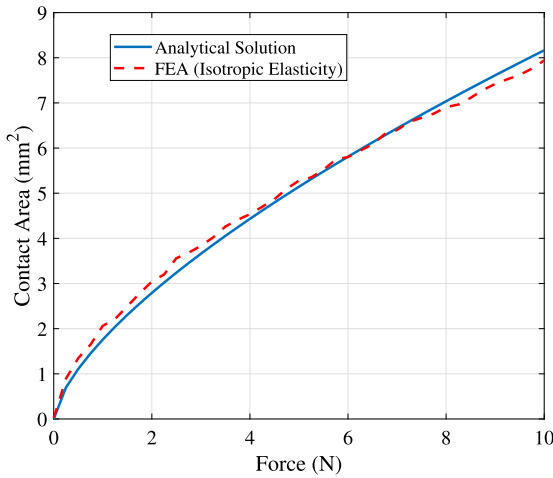


Fig. 10. Comparison of the analytical Hertz solution and the FEM static response.

## 5. Sensor design optimization

In the OPT-IN sensor under investigation, two key elements play a critical role in determining its overall performance, and optimizing them is essential for achieving high efficiency. The first element is the shape of the probe, which directly influences the interaction with the target medium and affects the sensitivity and accuracy of the sensor. The second element is the detector, whose selection can significantly impact the signal quality, response time, and reliability of measurements. By carefully choosing both the probe geometry and the detector type, it is possible to enhance the sensor's dynamic range and minimize measurement errors. Therefore, in the following subsections, a detailed

examination of these two crucial elements is presented to highlight their influence on sensor functionality.

### 5.1. Probe shape investigation

For the probe, different shapes (bigger cylinder radius = 8.5 mm, smaller cylinder radius = 2.2 mm, paraboloid shape parameters  $a = 3$   $b = 4$ , sphere radius = 3 mm,) (illustrated in Fig. 13) can be considered to study the contact area between the probe and the waveguide. As discussed in earlier sections, the contact problem is determined solely by the geometries of the two bodies through the combined initial gap function. This function, which is the sum of the initial gaps of each body, can be expressed as:

$$g_0(x, y) = Ax^2 + By^2 + Cxy \quad (10)$$

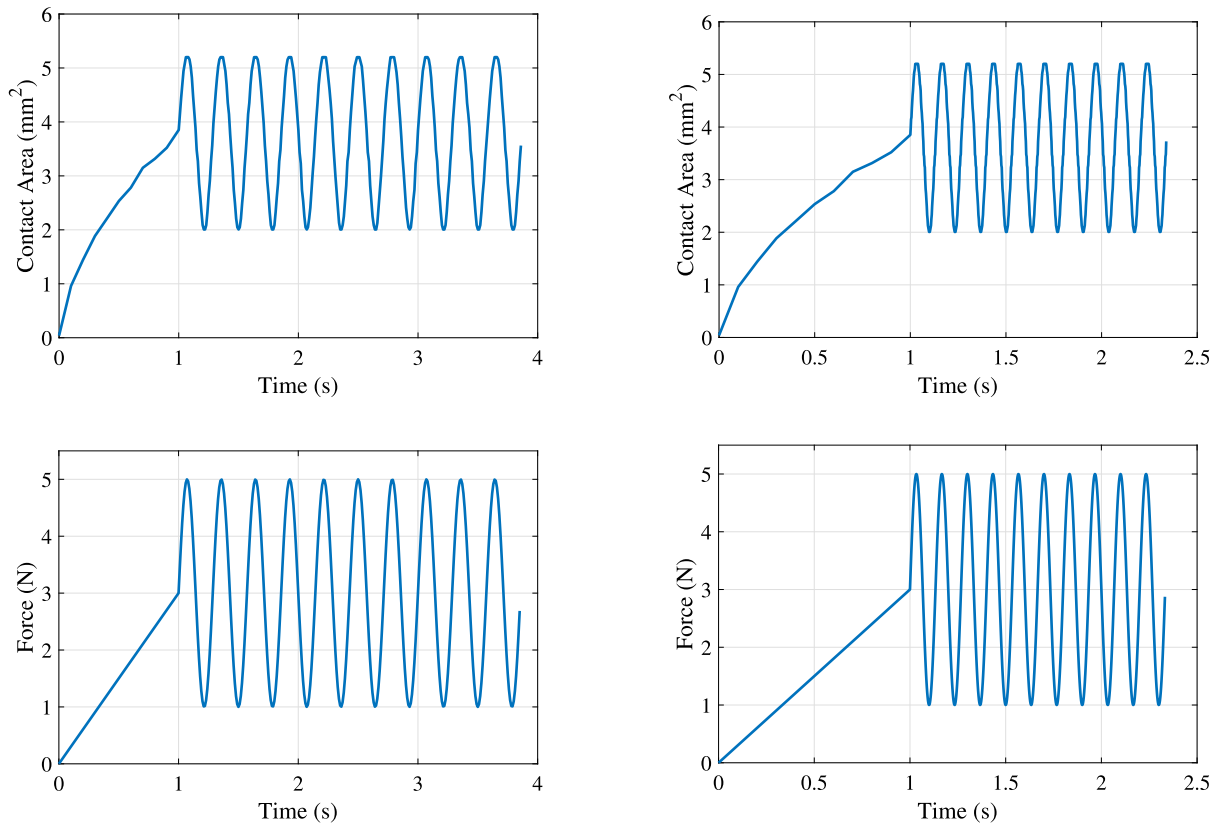
The elastic contact problem corresponding to the gap function defined in Eq. (10) was first addressed by Hertz (1882) [26]. Eq. (10) can be applied to general ellipsoidal bodies when the principal curvatures of the two bodies are aligned, in which case it can be simplified as follows:

$$g_0(x, y) = Ax^2 + By^2 = \frac{x^2}{2R_x^{(1)}} + \frac{y^2}{2R_y^{(1)}} + \frac{x^2}{2R_x^{(2)}} + \frac{y^2}{2R_y^{(2)}} \quad (11)$$

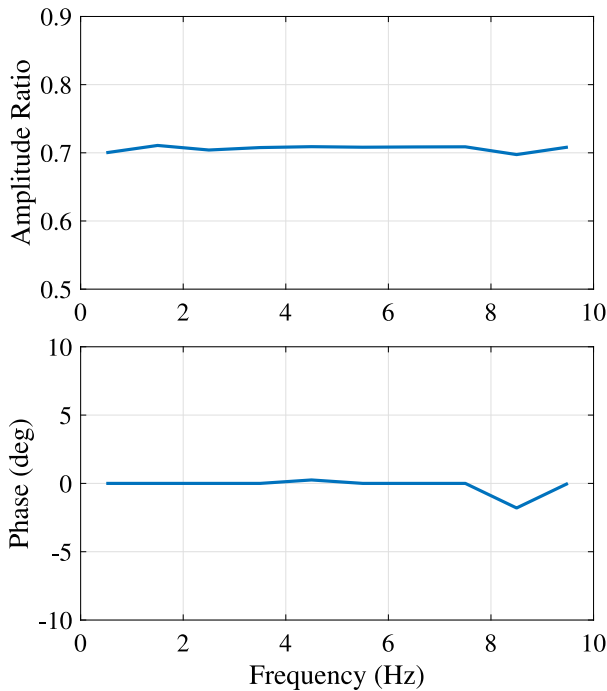
Here,  $R_x$  and  $R_y$  denote the principal radii of curvature of the surface, and the superscripts (1) and (2) indicate the corresponding radii of bodies 1 and 2, respectively. The parameters  $A$  and  $B$  may vary for the contacting bodies shown in Fig. 13. For details on the calculation of these parameters, readers are referred to [26]. The resulting contact areas for the shapes illustrated in Fig. 13 are presented in Fig. 14.

From Fig. 14, it can be observed that all cases exhibit a similar trend: an initial nonlinear region followed by an approximately linear region. However, within this second portion of the curve, a perfectly linear response is not present. To improve accuracy, the quasi-linear region is examined in order to identify the force range where the curve behaves in a strictly linear manner. For this purpose, the cylinder-sphere configuration is selected as a case study, and a curve-fitting procedure is applied to determine the most linear segment. Based on the sensor's operational force range, the optimal interval is then identified for the intended application. The results of this curve fitting are shown in Fig. 15 and Table 4 reports the parameter values.

Table 4 presents the curve-fitting parameters for various force and contact area ranges. The results show that the fitted curves provide an excellent agreement with the data, as indicated by the R-square values, all greater than 0.999. This demonstrates that the linear approximation accounts for nearly all of the variability in the experimental results within the examined force intervals. The RMSE values are also



**Fig. 11.** Transient response comparison: left column corresponds to 3.5 Hz and right column to 7.5 Hz. The top row shows contact area, and the bottom row shows force response.



**Fig. 12.** Frequency response function of the sensing unit.

**Table 4**

Force and contact area ranges with corresponding fit statistics.

| Force (N) range | Area (mm <sup>2</sup> ) range | R-square | RMSE (mm <sup>2</sup> ) |
|-----------------|-------------------------------|----------|-------------------------|
| 3.9–6.6         | 5.38–7.64                     | 0.9995   | 0.0163                  |
| 6–10            | 7.17–10.08                    | 0.9995   | 0.0197                  |
| 8–12            | 8.68–11.38                    | 0.9997   | 0.0145                  |
| 10–14           | 10.08–12.61                   | 0.9998   | 0.0114                  |
| 10.5–16.2       | 10.41–13.90                   | 0.9996   | 0.0197                  |
| 12–18           | 11.38–14.90                   | 0.9997   | 0.0186                  |

very low, ranging from 0.0114 mm<sup>2</sup> to 0.0197 mm<sup>2</sup>, confirming that the discrepancies between the fitted curves and the experimental data are minimal relative to the overall contact area scale. The smallest RMSE (0.0114 mm<sup>2</sup>) is found in the 10–14 N force range, highlighting this interval as the most precise representation of linear behavior. Slightly larger RMSE values are observed for broader ranges, such as 6–10 N and 10.5–16.2 N, suggesting that extending the fitting range may slightly reduce accuracy. Nonetheless, all RMSE values remain well within acceptable limits, confirming the stability of the linear approximation. These findings validate that the force–area relationship can be modeled with high precision, providing a reliable foundation for defining the optimal operating range of the sensor. Based on Table 4, it is also possible to apply an initial preload to the probe holder to eliminate the nonlinear portion of the curve, thereby isolating the desired linear range for practical applications.

## 5.2. Detectors

In this subsection, three light detectors are selected to measure the transmission from the waveguide to the probe, and their output signals are analyzed. The chosen detectors are the BPW34 and BPW21R photodiodes, along with a light-dependent resistor (LDR).

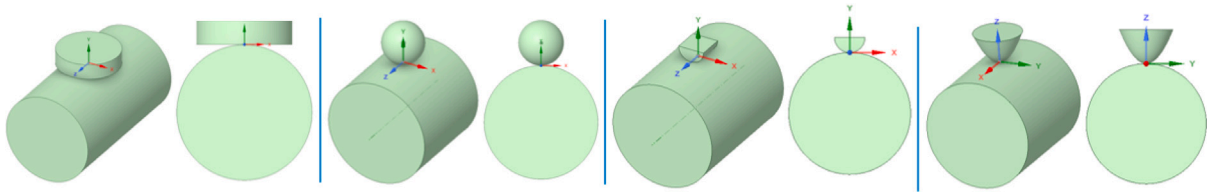


Fig. 13. Probe geometries considered in the design study.

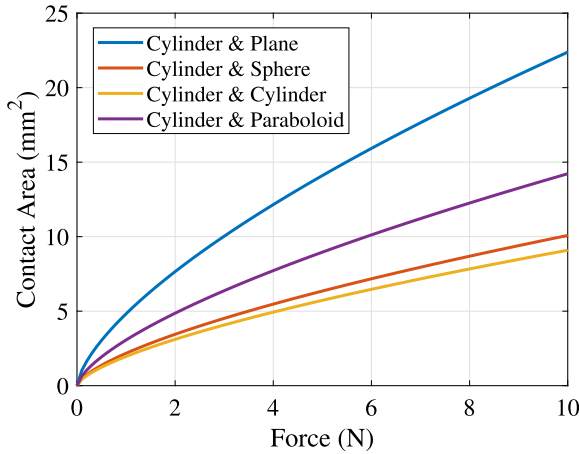


Fig. 14. Force-contact-area curves for the investigated probe geometries.

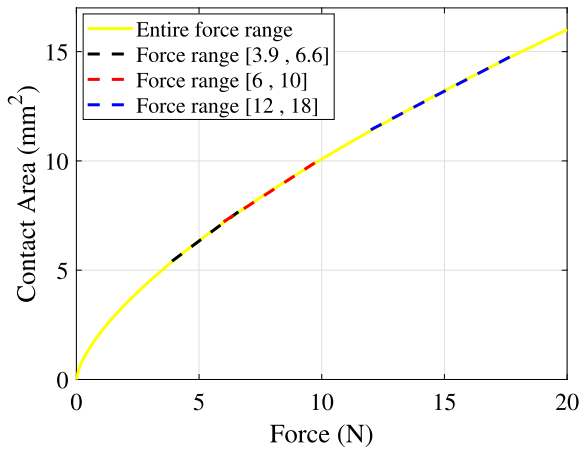


Fig. 15. Force ranges and corresponding contact-area intervals.

The detectors were integrated into a flight control grip (designed for helicopter applications) that was fabricated using a 3D printer (Fig. 16). As illustrated in Fig. 16, the controller houses the detectors, which are connected to dedicated circuits, and their signals are acquired through a data acquisition system at a 10Hz sampling rate. The figure also presents the CAD model used for constructing the prototype, showing the central core containing the waveguide along with four sensing units equipped with detectors.

The first performed comparison of the three detectors relies on the signal-to-noise ratio (SNR) [34]:

$$\text{SNR}_{\text{dB}} = 20 \log_{10} \left( \frac{\mu}{\sigma} \right),$$

where  $\mu$  denotes the average signal value and  $\sigma$  represents the standard deviation of the noise [35]. Within this framework, the LDR is recognized for its simple design and strong sensitivity to light intensity, though it typically exhibits slower response dynamics. In contrast,

photodiodes such as the BPW34 and BPW21R are semiconductor-based detectors that provide rapid response times, enhanced stability, and better linearity across a wide spectral range. The experimental SNR results clearly distinguish the detectors in terms of performance. The LDR recorded the highest SNR of 66.88 dB, highlighting its ability to deliver the cleanest signal with minimal noise contamination. The BPW34 showed an intermediate performance with an SNR of 64.77 dB, maintaining good reliability though with slightly higher noise levels compared to the LDR. The BPW21R exhibited the lowest SNR of 61.61 dB, indicating stronger susceptibility to noise, yet still producing valid and usable outputs. Overall, all three devices demonstrate acceptable signal detection capabilities, but the selection of the most suitable detector depends on the quality demands of the application. The LDR is most advantageous where high signal clarity is required, whereas the photodiodes are better suited for contexts that favor rapid response and semiconductor robustness, even if accompanied by lower SNR values. For the time histories of the voltage signals of the three detectors during the tests, refer to [Appendix B](#).

## 6. Metrological assessment experiments

In this section, several experiments are conducted to evaluate the performance of the proposed force sensor (OPT-IN). Fig. 17 shows the probe testing setup equipped with three detectors: LDR, BPW34, and BPW21R. In the probe tester, the waveguide is enclosed by a black hollow cylinder containing three circular openings at the top to accommodate the photodetectors. An LED is mounted on the left side of the waveguide, with its optical axis oriented perpendicular to the cylinder axis. The probe is positioned on the cylinder at the designated circular opening. The entire assembly is enclosed within a rectangular aluminum housing, which minimizes light leakage from the system and effectively blocks ambient light from entering the probe tester, thereby ensuring stable optical measurement conditions. A hydraulic testing system (MTS 858 Mini Bionix II™) available at Politecnico di Milano is employed to apply the necessary force and displacement. First, an experiment is performed to validate the FE model. The remaining tests comprise static calibration, hysteresis, repeatability, drift, and dynamic tests at several frequencies. The waveguide used in these experiments has a diameter of 21 mm. Furthermore, the temperature of the waveguide is monitored to confirm that the surface temperature remains suitable and safe for silicone probe attachment, as well as to determine how strongly the LED COB affects the waveguide temperature. Details for each test are provided in the following subsections.

### 6.1. Model validation

Because the probe-waveguide contact area could not be measured directly in the present setup, the experimental optical response was used as an indirect indicator of contact-area evolution. In this subsection, the numerical model is validated by comparing its predictions with the corresponding experimental results. To facilitate a direct comparison, both the numerical contact-area and experimental optical-response datasets were normalized using the Min-Max normalization method. This approach removes differences in scale and enables a more meaningful evaluation of the agreement between the predicted and measured responses. During the experiment, the silicone probe was

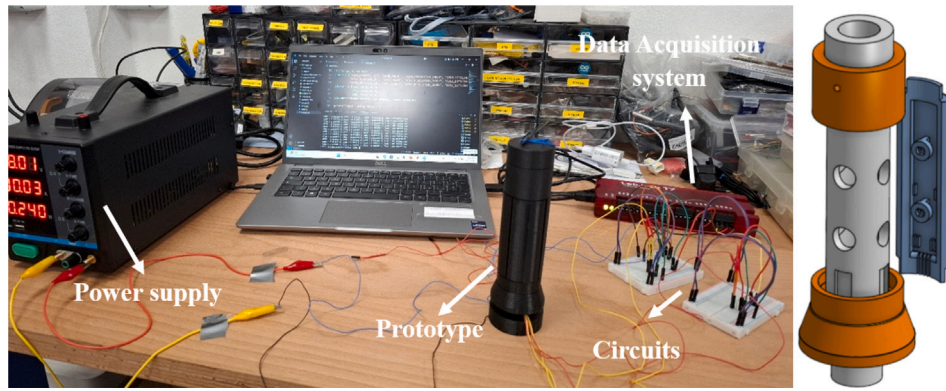


Fig. 16. Experimental setup and prototype used in the detector study.

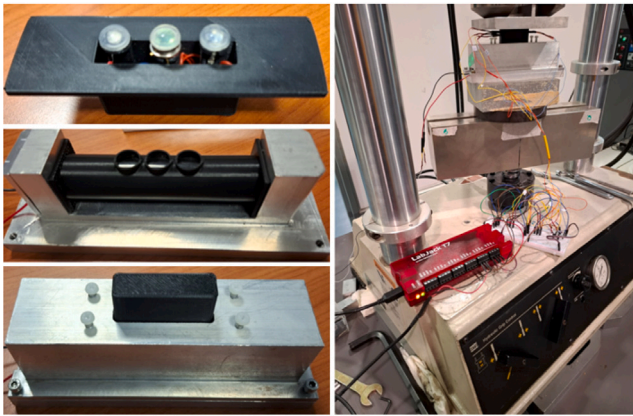


Fig. 17. Experimental setup.

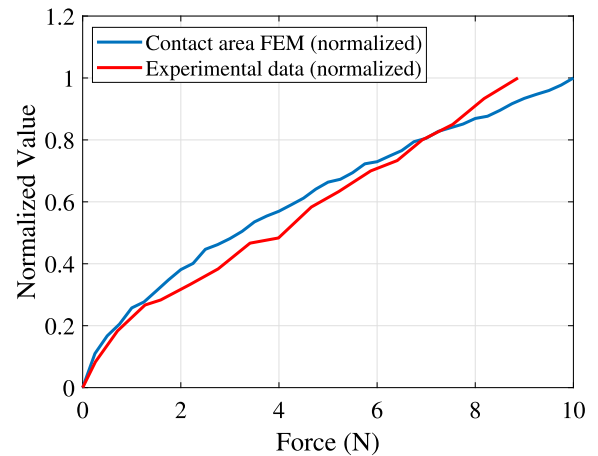


Fig. 18. Comparison between the normalized experimental response and linear-isotropic FEM prediction.

gradually compressed against the cylindrical waveguide in increments of 0.1 mm. The experimental data used in this study were adopted from the authors' previous work [22] because the waveguide geometry is identical to that employed in the finite element model. Fig. 18 presents the comparison between the normalized numerical contact-area and experimental optical responses. The comparison demonstrates similar trends in the normalized numerical contact area and experimental optical response as loading increases. Both datasets exhibit a nonlinear increase during the initial loading stage, followed by an approximately linear relationship over the remaining loading range, indicating that the numerical model successfully captures the essential mechanical behavior of the system. Slight deviations are observed between the FEM predictions and the experimental data, but overall the agreement between the normalized curves supports that the finite element model provides a reliable representation of the system behavior.

## 6.2. Static sensor calibration

Static calibration tests were conducted to establish the linear relationship between the applied compressive force and the corresponding output signal of the sensor. To ensure contact between the sphere and the cylinder and to minimize the effect of the initial nonlinearity discussed in the previous sections, an initial displacement of 0.3 mm was applied before the measurements. For the calibration procedure, the load cell of the MTS testing machine applied a displacement increment of 0.1 mm every 10 s during the loading phase. The corresponding force was recorded at each step. The loading procedure was performed over a force range of 0–10 N. After reaching the maximum displacement, the unloading phase was conducted by reversing the procedure. The displacement was decreased by 0.1 mm every 10 s, and the corresponding

Table 5

Statistical metrics of the calibration results for different detectors.

| Statistical metrics             | LDR   | BPW34 | BPW21R |
|---------------------------------|-------|-------|--------|
| $R^2$                           | 0.89  | 0.93  | 0.93   |
| Standard deviation of error (%) | 5.31  | 2.79  | 2.93   |
| RMSE (%)                        | 5.29  | 2.78  | 2.92   |
| Mean absolute error (%)         | 4.15  | 2.17  | 2.36   |
| Maximum absolute error (%)      | 13.61 | 8.32  | 9.12   |
| Minimum absolute error (%)      | 0.023 | 0.038 | 0.002  |

force was recorded at each step until the displacement returned to its initial position.

The analog signals were acquired using a LabJack T7 data acquisition device at a sampling rate of 100 Hz. The T7 employs a 24-bit sigma-delta analog-to-digital converter with internal oversampling and digital decimation filtering. No additional digital filtering was applied to the acquired signals. The experiment was repeated nine times to ensure the reliability and repeatability of the measurements. The collected data were then analyzed to evaluate the sensor's linearity. Fig. 19 presents the linear regression fitting of the measured data, which collectively validate the sensor's quasi-linear response under static conditions. Moreover, the linear calibration curve indicates a linear relationship between the applied force and the corresponding output voltage and is used to calibrate the LDR, BPW34, and BPW21R detectors.

Table 5 summarizes the statistical parameters associated with the calibration curves presented in Fig. 19. The BPW34 exhibited the best calibration performance, with the highest coefficient of determination

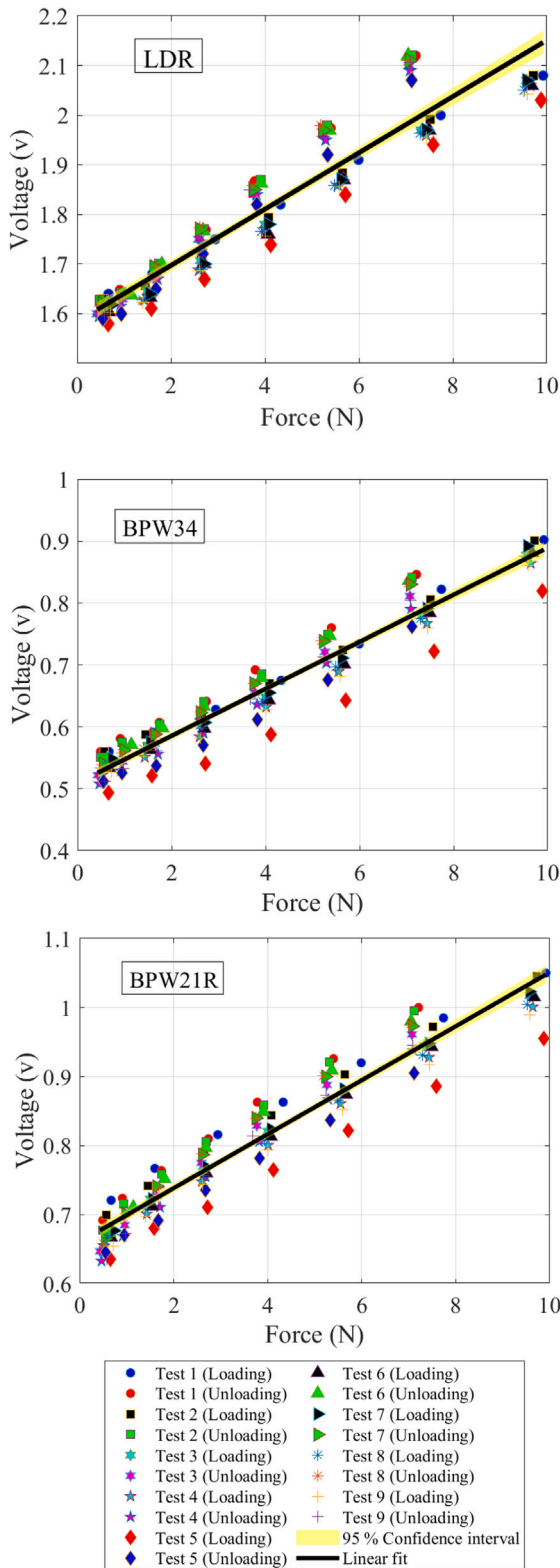


Fig. 19. Quasi-linear responses of the detectors under static loading. The legend is common to all graphs.

( $R^2 = 0.93375$ ), indicating the strongest linear relationship between the sensor output and the applied force. The BPW21R showed comparable performance ( $R^2 = 0.93073$ ), while the LDR presented the lowest coefficient of determination ( $R^2 = 0.89663$ ), indicating a larger deviation from the fitted calibration curve.

Based on the obtained calibration equations, the experimental data were compared with the predicted values, and the corresponding error metrics, including standard deviation, RMSE, MAE, maximum absolute error, and minimum absolute error, are reported in Table 5. The BPW34 achieved the lowest standard deviation (2.79 %), RMSE (2.78 %), and MAE (2.17 %), indicating lower measurement dispersion, better agreement with the calibration model, and improved force estimation accuracy. The BPW21R showed a similar performance, with slightly higher errors (standard deviation = 2.93 %, RMSE = 2.92 %, and MAE = 2.36 %), whereas the LDR exhibited larger deviations (standard deviation = 5.31 %, RMSE = 5.29 %, and MAE = 4.15 %), indicating higher variability in force estimation.

The maximum absolute error followed the same trend, with the BPW34 showing the lowest deviation (8.32 %), followed by the BPW21R (9.12 %) and LDR (13.61 %). Although all detectors achieved very small minimum absolute errors, the overall error distribution demonstrates that the BPW34 provides the most accurate and consistent calibration response, followed by the BPW21R, while the LDR exhibits comparatively higher uncertainty.

The 95 % confidence intervals for the BPW21R, LDR, and BPW34 detectors are all narrow and of similar width throughout the calibration range. This indicates that the uncertainty associated with the fitted calibration curves is low and nearly identical for all three detectors. Therefore, each detector exhibits stable and statistically reliable calibration behavior despite slight differences in sensitivity.

### 6.3. Hysteresis tests

Hysteresis is defined as the maximum deviation in a sensor's output between the loading and unloading cycles for the same applied force level [36]. To evaluate this characteristic, an experiment was designed in which the displacement was gradually increased from 0.3 mm to 0.8 mm in increments of 0.1 mm every 20 s. After reaching the total displacement of 0.5 mm, the procedure was reversed to reduce the displacement. Five distinct force levels were specified; they were evaluated after the signal had stabilized.

For each force level, the difference between the output signals during loading and unloading was calculated to quantify the hysteresis error. The experimental results (Table 6) indicate that the LDR exhibits the highest maximum hysteresis (8.42 %), while the BPW34 and BPW21R photodiodes show significantly lower values of 3.35 % and 2.55 %, respectively. A similar trend is observed for the mean hysteresis, with values of 7.05 %, 1.52 %, and 1.96 % for the LDR, BPW34, and BPW21R, respectively, indicating that the photodiodes maintain lower hysteresis throughout the entire loading–unloading cycle rather than only at a single operating point. Moreover, the BPW21R exhibits the lowest hysteresis standard deviation (0.55 %), demonstrating the most stable hysteresis behavior, while the BPW34 achieves the lowest RMS hysteresis (1.82 %), indicating the smallest overall hysteresis error. For more details refer to Appendix B. These results suggest that the photodiode sensors provide more consistent and repeatable performance under cyclic loading, making them more suitable for dynamic force sensing applications.

### 6.4. Static repeatability and drift tests

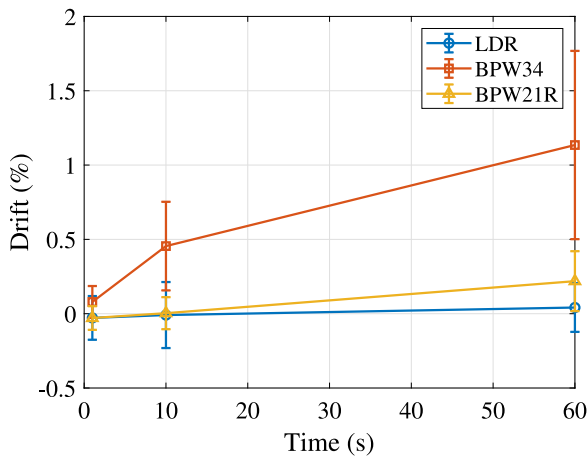
The static repeatability and drift tests are conducted to evaluate the consistency and stability of the sensor output under steady loading conditions. The repeatability test examines how reliably the sensor reproduces its output when the same static load is applied multiple times under identical conditions, while the drift test assesses the temporal

**Table 6**  
Statistical metrics related to the hysteresis test for different detectors.

| Statistical metrics    | LDR  | BPW34 | BPW21R |
|------------------------|------|-------|--------|
| Maximum hysteresis (%) | 8.42 | 3.35  | 2.55   |
| Mean hysteresis (%)    | 7.05 | 1.52  | 1.96   |
| Hysteresis STD (%)     | 1.59 | 1.11  | 0.55   |
| RMS hysteresis (%)     | 7.20 | 1.82  | 2.02   |

**Table 7**  
Statistical metrics related to the repeatability test for different detectors.

| Statistical metric           | LDR         | BPW34       | BPW21R      |
|------------------------------|-------------|-------------|-------------|
| Mean (V)                     | 1.979       | 0.749       | 0.873       |
| Standard deviation (V)       | 0.026       | 0.069       | 0.071       |
| Max. repeatability error (%) | 1.581       | 11.016      | 9.107       |
| 95 % confidence interval (V) | $\pm 0.018$ | $\pm 0.049$ | $\pm 0.051$ |



**Fig. 20.** Drift of the three detectors over 60 s.

stability of the sensor's signal when the load is held constant [36]. Low variations in repeated measurements and minimal signal fluctuation over time indicate high repeatability and low drift, reflecting the overall reliability of the sensor's performance. Ten repeated tests were performed for each detector. For the drift calculation, the stabilized output was taken as the reference and subsequent values were recorded after 1, 10 and 60 s. Refer to [Appendix B](#) for graphs showing the acquired waveforms. Signals were acquired at 100 Hz.

The results of the static repeatability test ([Table 7](#)) indicate that the LDR provides the best repeatability performance among the three detectors. It exhibits the lowest maximum repeatability error (1.58 %), together with the smallest standard deviation (0.026 V) and the narrowest 95 % confidence interval ( $\pm 0.018$  V), indicating high measurement consistency and low variability over repeated tests. In comparison, the BPW34 and BPW21R photodiodes show higher maximum repeatability errors of 11.02 % and 9.11 %, respectively, accompanied by larger standard deviations (0.069 V and 0.071 V) and wider 95 % confidence intervals ( $\pm 0.049$  V and  $\pm 0.051$  V), suggesting greater dispersion in the repeated measurements. Although the BPW34 has the lowest mean output value (0.749 V), followed by the BPW21R (0.873 V) and the LDR (1.979 V), the repeatability is primarily reflected by the variability of the measurements rather than their mean values. These results indicate that the LDR produces the most stable response under repeated loading conditions, whereas the photodiodes are more sensitive to small variations in optical coupling or alignment, leading to larger measurement fluctuations.

Based on the drift test results shown in [Table 8](#), the LDR sensor demonstrates minimal drift over time, with mean values ranging from  $-0.03$  % at 1 s to  $0.04$  % at 60 s and low standard deviations ( $0.14$ – $0.22$  %), indicating excellent stability. In contrast, the BPW34 shows a more

**Table 8**  
Statistical results related to drift test of different detectors over time.

| Detectors |      | Drift (0 s) | Drift (1 s) | Drift (10 s) | Drift (60 s) |
|-----------|------|-------------|-------------|--------------|--------------|
| LDR       | Mean | 0 %         | $-0.03$ %   | $-0.01$ %    | $0.04$ %     |
|           | Std  | 0 %         | $0.14$ %    | $0.22$ %     | $0.16$ %     |
| BPW34     | Mean | 0 %         | $0.08$ %    | $0.45$ %     | $1.13$ %     |
|           | Std  | 0 %         | $0.11$ %    | $0.29$ %     | $0.63$ %     |
| BPW21R    | Mean | 0 %         | $-0.03$ %   | $0.003$ %    | $0.21$ %     |
|           | Std  | 0 %         | $0.08$ %    | $0.10$ %     | $0.20$ %     |

pronounced drift, increasing from  $0.08$  % at 1 s to  $1.13$  % at 60 s, with higher variability (standard deviation up to  $0.63$  %), suggesting it is less suitable for long-term stable measurements. The BPW21R exhibits intermediate behavior, with small mean drift values ( $-0.03$ – $0.21$  %) and relatively low standard deviations ( $0.08$ – $0.2$  %), making it more stable than the BPW34 while slightly less stable than the LDR ([Fig. 20](#)). Overall, the results indicate that the LDR provides the most consistent performance over time, BPW21R offers moderate stability, and BPW34 shows the highest drift among the tested sensors.

### 6.5. Dynamic tests

The dynamic load experiments reveal distinct variations in how the sensors respond to a sinusoidal input applied at frequencies of 2, 4, 6, 8 and 10 Hz, with data recorded at 500 Hz to ensure high-resolution capture. Following each test, the frequency response function (FRF) was computed, and the corresponding results are presented in [Fig. 21](#). Additional details can be found in [Appendix B](#).

The FRF analysis shows that the amplitude response of all sensors decreases gradually with increasing excitation frequency, indicating a mild attenuation effect. The LDR exhibits a nearly linear reduction in amplitude from  $0.9585$  at 2 Hz to  $0.8597$  at 10 Hz, suggesting limited dynamic sensitivity at higher frequencies. The BPW34 photodiode maintains the highest amplitude across the tested range, reflecting superior dynamic stability and faster photonic response. Conversely, the BPW21R shows the lowest amplitude overall, implying higher susceptibility to frequency-dependent losses. Over the tested frequency range, the phase differences of all sensors increase approximately linearly with frequency. This behavior is consistent with an approximately constant effective time delay rather than the phase response of a first-order pole. Among them, BPW21R demonstrates the steepest phase shift, reaching  $50.08^\circ$  at 10 Hz, indicating a larger effective delay than that of the BPW34. Overall, the BPW34 outperforms the others in both amplitude retention and phase stability, making it the most suitable detector for dynamic measurements up to 10 Hz.

### 6.6. Waveguide temperature

The silicone probe may be influenced by the temperature of the waveguide surface, as elevated surface temperatures can alter the mechanical and optical properties of the silicone material, potentially leading to structural changes. In addition, excessive heat can adversely affect the detector response, resulting in inaccurate measurements. Therefore, in this subsection, a temperature sensor (Texas Instruments LM34) is employed to measure and evaluate the temperature of the waveguide under illumination from the LED COB, with the temperature reported in  $^\circ\text{C}$ .

The temperature sensor is installed on the waveguide at two different locations to measure its surface temperature. In Position 1, the LM34 is placed close to the waveguide, without direct contact, while in Position 2, the sensor is directly in contact with the waveguide ([Fig. 22](#)). Each configuration is tested for 1 h, and the temperature signal is recorded using a LabJack T7 data acquisition system. The corresponding results are presented in [Appendix B](#).

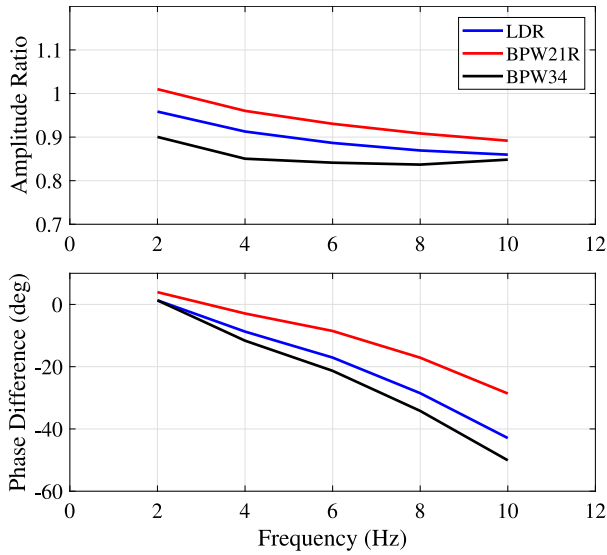


Fig. 21. Frequency response function of the three light detectors.

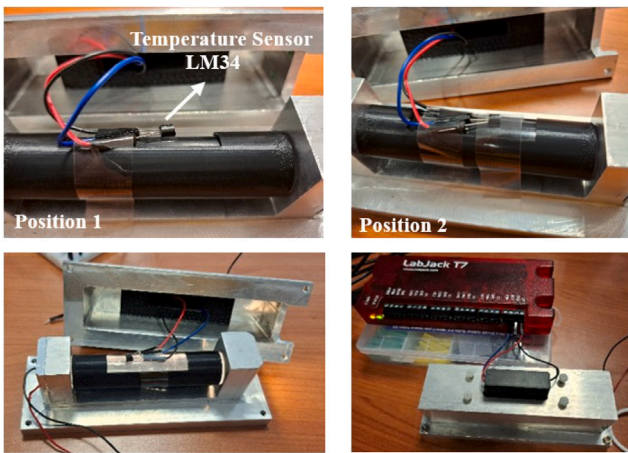


Fig. 22. Waveguide-temperature test: experimental configuration.

The results indicate that the waveguide surface temperature remains stable over the 1 h measurement period for both configurations, with values consistently close to 27 °C. This temperature range is well within the safe operating limits for silicone materials, indicating that no thermally induced changes in the probe's material properties are expected. Furthermore, the minimal temperature variation suggests that thermal effects do not significantly influence the probe structure or degrade the detector performance, thereby ensuring reliable and accurate measurements.

### 6.7. Metrological performance comparison

The proposed OPT-IN sensor is compared with several optical tactile sensors in terms of performance, components, and system complexity. It should be noted that this comparison is not intended to determine which sensor is superior; rather, it aims to provide the means for discerning the fitness of the proposed solution to a specific measurement application.

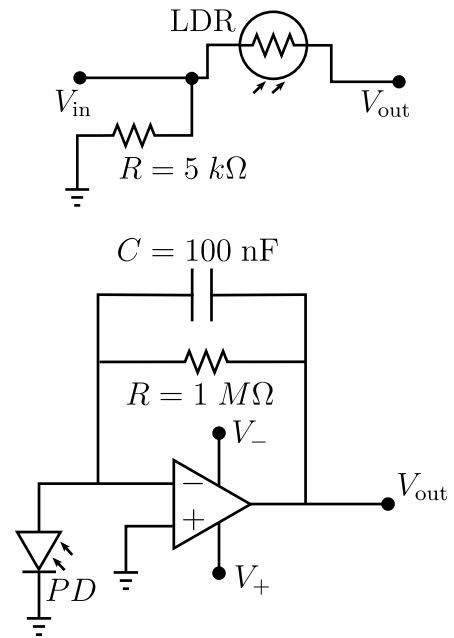


Fig. 23. Signal-conditioning circuits: (a) LDR voltage divider and (b) photodiode transimpedance amplifier.

For comparison, an optical waveguide-based tactile sensor [37], a fiber-optic tactile force sensor [38], and two GelSight tactile sensors [39,40] were selected. The metrological performance of optical tactile sensors can be evaluated using key metrics, including hysteresis, calibration error, root mean square error (RMSE), coefficient of determination ( $R^2$ ), and repeatability. Table 9 compares the performance metrics for the selected sensors. The results indicate that the proposed OPT-IN sensor exhibits RMSE, hysteresis, and repeatability values within the range reported for existing optical tactile sensors, demonstrating comparable metrological performance.

The complexity and cost of optical tactile sensors are largely determined by their sensing components, including optical sources, detectors, imaging systems, and signal-processing requirements. Based on the bill of materials listed in Table 10, the estimated cost of the proposed OPT-IN sensor is approximately 13 USD when the BPW21R photodiode is used, whereas the versions employing the LDR or BPW34 detector cost less than 8 USD. Including the silicone probe (0.11 USD) and the polycarbonate waveguide (approximately 2.75 USD for 100 cm length), the total material cost is approximately 15 USD per sensor for the BPW21R configuration and around 10 USD for the LDR and BPW34 configurations. In addition, the cost of the 3D-printed housing depends on the specific application and manufacturing method and is therefore not included in the estimates. For comparison, Li et al. [40] reported an estimated cost of approximately 31 USD for a GelSight tactile sensor consisting of a camera, LEDs, mirrors, a gel layer, a 3D-printed housing, springs, electronic components, a battery, and general assembly materials (e.g., adhesives). Beyond cost, the proposed sensor also offers reduced system complexity. Camera-based optical tactile sensors, such as GelSight and total internal reflection (TIR)-based systems, provide high-resolution spatial information by capturing deformation-induced optical patterns. However, these approaches require integrated imaging systems, controlled illumination, precise optical alignment, and computationally intensive image-processing algorithms. In contrast, the proposed OPT-IN sensor employs a direct photodetection approach, eliminating the need for cameras and image reconstruction while reducing both hardware complexity and computational requirements.

**Table 9**

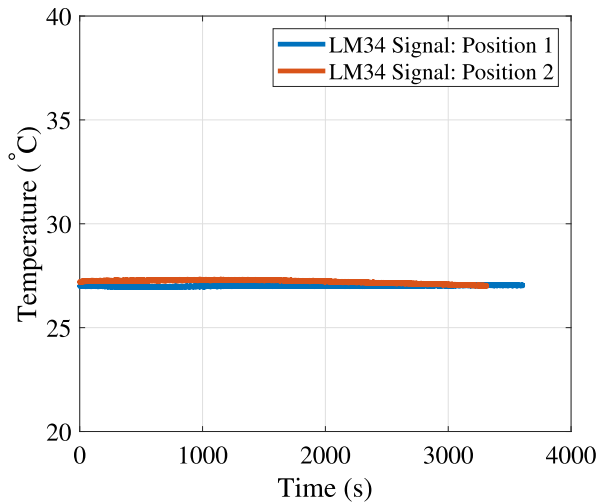
Performance comparison between the proposed OPT-IN sensor and other optical tactile sensors.

| Performance parameter | OPT-IN (LDR) | OPT-IN (BPW34) | OPT-IN (BPW21R) | Wireless GelSight sensor (Fx) [40] | GelSight-based sensor [39] | Fiber-based sensor [38] | Waveguide-based sensor [37] |
|-----------------------|--------------|----------------|-----------------|------------------------------------|----------------------------|-------------------------|-----------------------------|
| $R^2$                 | 0.89         | 0.93           | 0.93            | 0.96                               | 0.94                       | –                       | 0.97                        |
| RMSE                  | 5.29 %       | 2.78 %         | 2.92 %          | 0.23 N                             | 1.856 N                    | –                       | –                           |
| Hysteresis            | 8.42 %       | 3.35 %         | 2.55 %          | 13 %                               | –                          | 3.6–8.17 %              | 2.63 %                      |
|                       | (Max)        | (Max)          | (Max)           |                                    |                            |                         |                             |
| Repeatability         | 1.58 %       | 11.01 %        | 9.10 %          | 10 %                               | –                          | –                       | 1.52 %                      |
| Drift                 | 0.04 %       | 1.13 %         | 0.21 %          | –                                  | –                          | 3.18 %                  | –                           |

**Table 10**

Electrical components used in the proposed tactile sensor.

| Electrical component           | Commercial brand and description                                                                      | Estimated cost (USD) |
|--------------------------------|-------------------------------------------------------------------------------------------------------|----------------------|
| LDR (Light dependent resistor) | Luna Optoelectronics NSL-19M51, TO-18 package, 50 mW                                                  | 1.27                 |
| BPW34 (Photodiode)             | Top-view photodiode, 430–1100 nm spectral range, $\pm 65^\circ$ field of view (Vishay Semiconductors) | 1.45                 |
| BPW21R (Photodiode)            | Photodiode with $50^\circ$ viewing angle, 300 mW (Vishay Semiconductors)                              | 9.87                 |
| Resistor                       | 5 k $\Omega$ –1 M $\Omega$                                                                            | 0.34                 |
| Capacitor                      | 100 nF                                                                                                | 0.11                 |
| Operational amplifier          | UA741                                                                                                 | 0.57                 |
| COB LED                        | Cree® XLamp® CXA1304 LED                                                                              | 2.07                 |



**Fig. 24.** Waveguide-temperature test: measured temperature time histories.

## 7. Conclusions

In this study, the tactile force sensor (OPT-IN) based on the Frustrated Total Internal Reflection (FTIR) principle was designed, modeled, and experimentally characterized. Through material evaluation, members of the silicone family—including Silicone (VMQ, heat-cured, 5–15 % fumed silica), Fluorosilicone (FVMQ, heat-cured, 10–30 % fumed silica), and Phenyl-type Silicone (PVMQ, heat-cured, 10–30 % fumed silica)—were identified as optimal candidates due to their high optical transparency and mechanical properties. Hertzian contact mechanics and a linear-isotropic FEM provided detailed insights into the sensor’s contact dynamics and deformation response. These analyses helped determine the static and dynamic response characteristics, enabling accurate prediction of the sensor’s optical behavior under load. The final configuration, involving an optimized probe geometry and detector type, ensured a stable and nearly linear correlation between applied force and optical output. Comprehensive testing was conducted to assess performance and reliability. Static calibration confirmed the linear force–voltage relationship, while hysteresis measurements showed a

maximum of about 8 % for the LDR, with the BPW34 and BPW21R photodiodes exhibiting considerably lower hysteresis. Repeatability and drift analyses demonstrated stable operation over time, validating detector consistency. Finally, dynamic frequency tests revealed smooth frequency response functions (FRFs), indicating predictable and consistent behavior under cyclic loading. Overall, the proposed OPT-IN tactile sensor demonstrates strong potential for precision tactile sensing in a wide range of engineering applications, where high sensitivity, repeatability, and optical robustness are essential.

## CRedit authorship contribution statement

**Ahmad Abbasi:** Writing – review & editing, Writing – original draft, Visualization, Validation, Software, Methodology, Investigation, Conceptualization. **Andrea Zanoni:** Writing – review & editing, Supervision, Project administration, Conceptualization.

## Declaration of Generative AI and AI-assisted technologies in the writing process

During the preparation of this work, the authors used LLM models to improve the language of the manuscript and the quality of the graphics. The authors subsequently reviewed and edited the content and take full responsibility for the publication.

## Declaration of competing interest

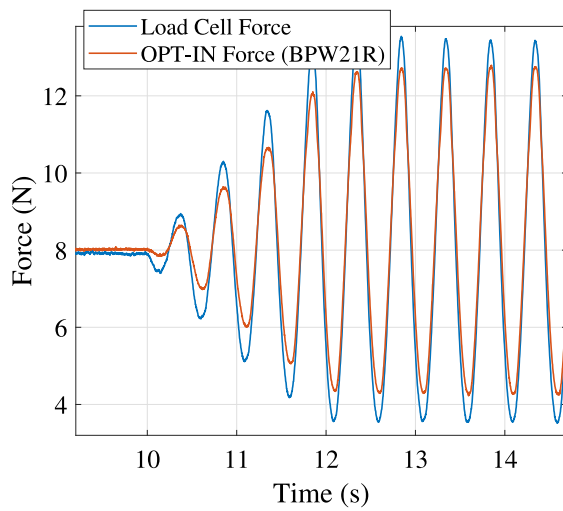
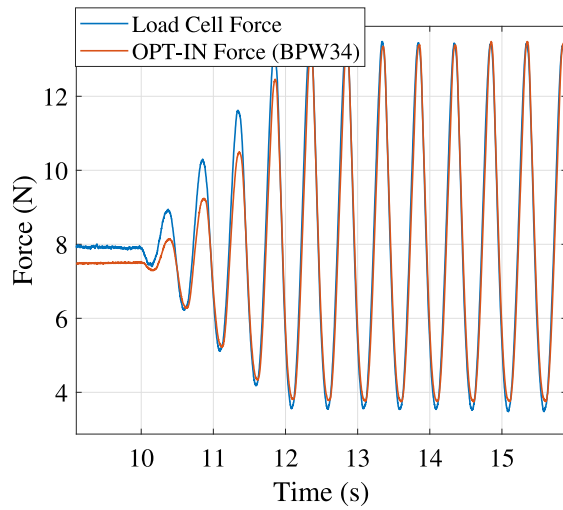
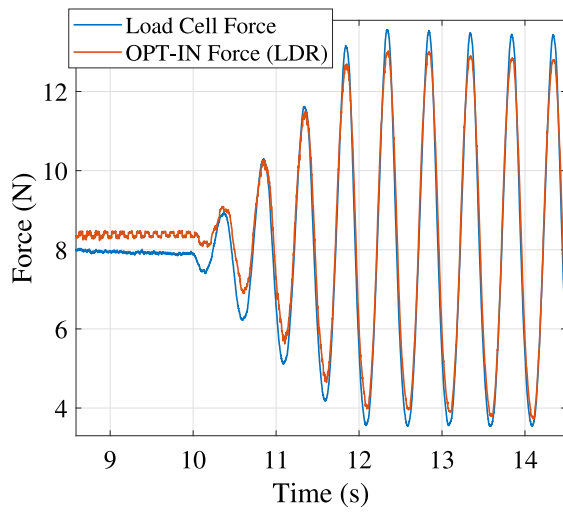
The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

## Acknowledgments

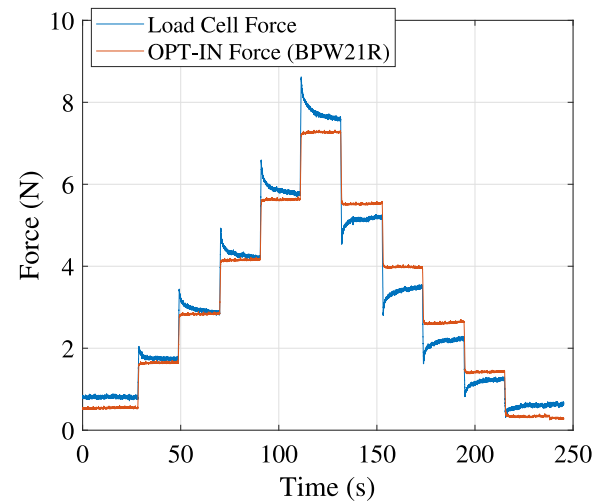
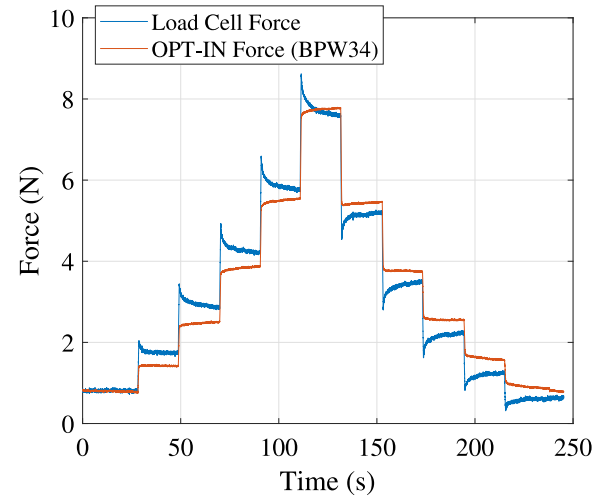
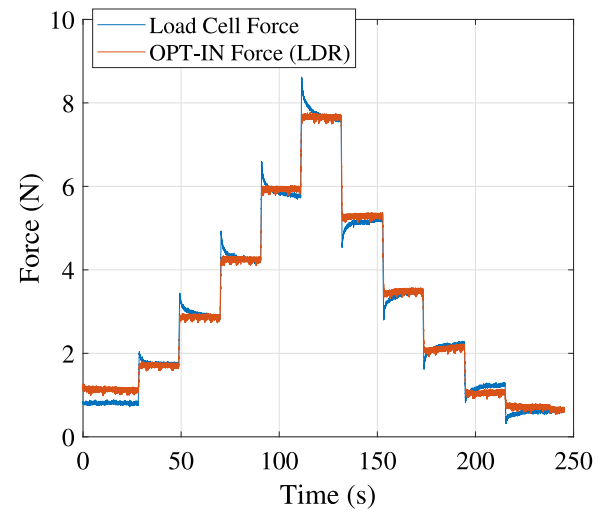
This work received funding through the  European Commission NextGenerationEU programme, through italian MISE PoC BOOSTECH implementation (CUP C48H23000470002).

## Appendix A. Electrical components and circuits used in the OPT-IN device

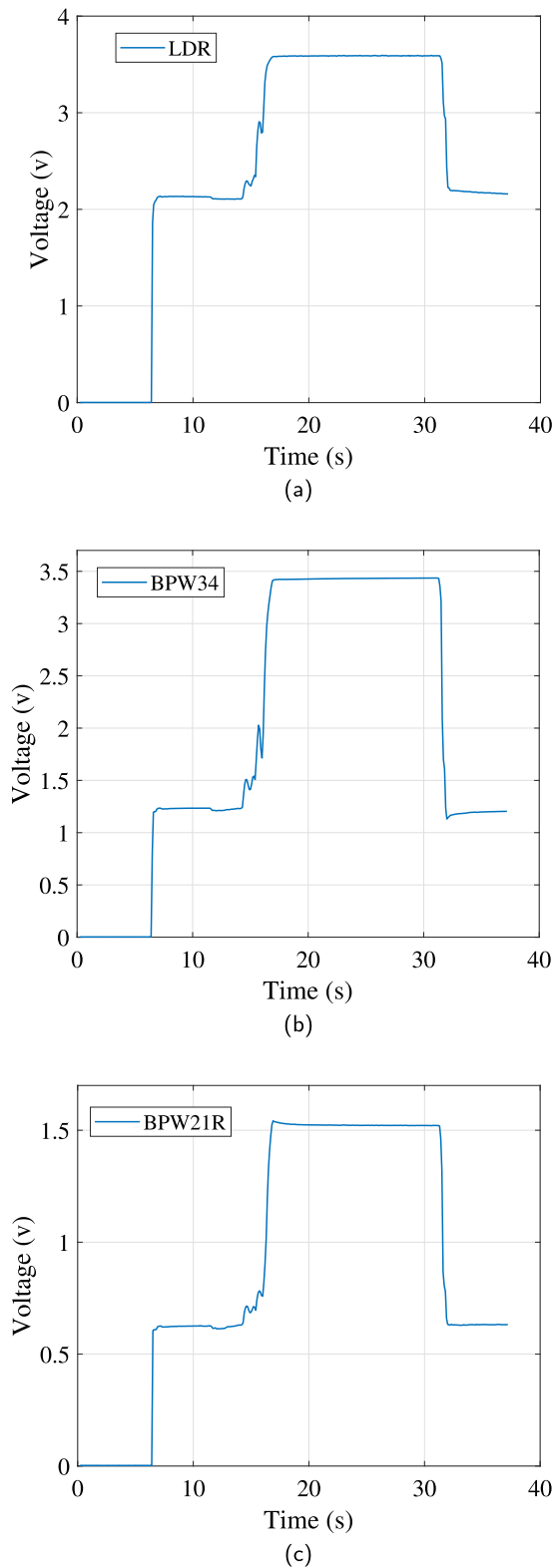
The electrical components used in the OPT-IN device are listed in [Table 10](#). The voltage-divider and transimpedance circuits are shown in [Fig. 23](#).



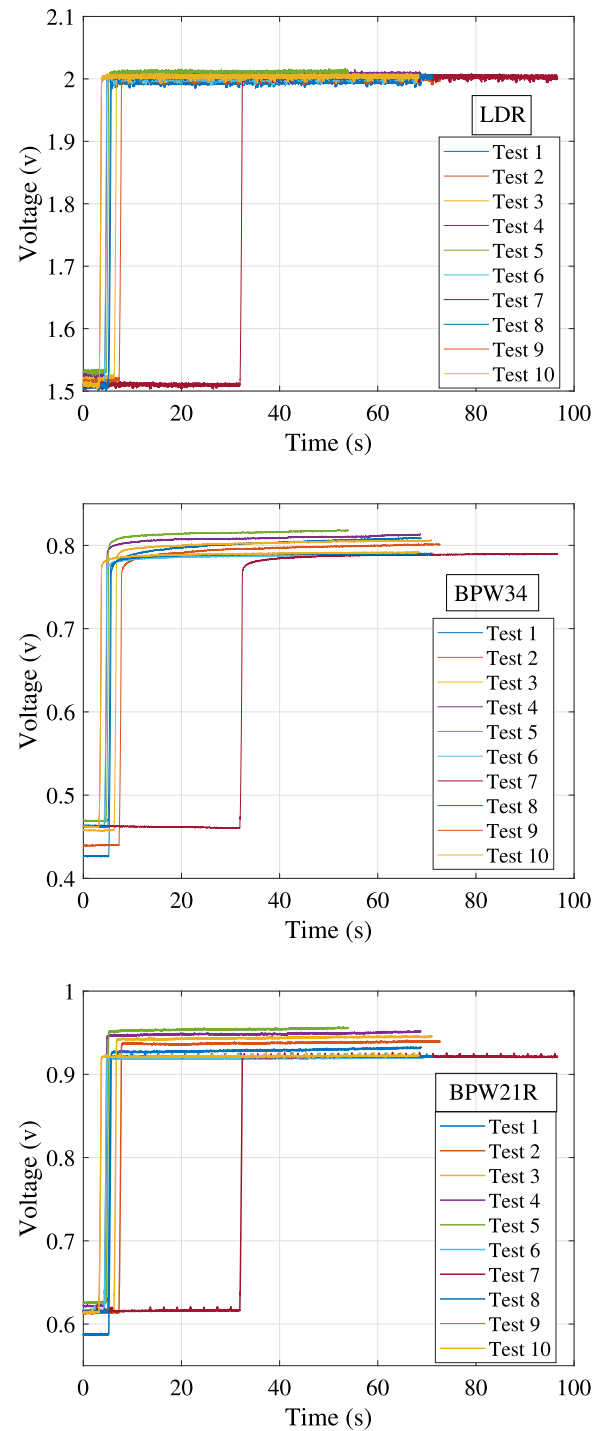
**Fig. 25.** Dynamic response at 2Hz for (a) the LDR, (b) the BPW34, and (c) the BPW21R.



**Fig. 26.** Hysteresis measurements for (a) the LDR, (b) the BPW34, and (c) the BPW21R.



**Fig. 27.** Signals obtained from the detectors during static SNR tests: (a) LDR, (b) BPW34, and (c) BPW21R.



**Fig. 28.** Representative repeatability and drift records for (a) the LDR, (b) the BPW34, and (c) the BPW21R.

## Appendix B. Sensor calibration and metrological assessment: additional material

Additional material for the sensor static calibration is collected in this section. The time-domain responses of the three detectors are illustrated in Fig. 25 for the 2 Hz case (Cf. Section 6.5). Fig. 24 shows the acquired waveforms of the temperatures on the OPT-IN waveguide in the 1 h test described in Section 6.6. Fig. 26 shows the time histories of the readings of OPT-IN, equipped with the three different light detectors, compared against the readings of the MTS 858 Mini Bionix

II™ load cell during hysteresis tests (Cf. Section 6.3). Fig. 27 shows the signals obtained during static Signal to Noise Ratio tests for the three detectors (Cf. Section 5.2). Notice that the tests start with the LED light switched off, corresponding to zero voltage output. Fig. 28 shows the signal obtained during the static repeatability tests (Cf. Section 6.4).

## Data availability

Data related to this work is publicly available at <https://codeberg.org/azanoni/OPT-IN-MEAS/>.

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