Enhanced transverse wavelength tuning of fiber Bragg gratings for sensing applications

Jinu, Paul

2006

Jinu, P. (2006). Enhanced transverse wavelength tuning of fiber Bragg gratings for sensing applications. Doctoral thesis, Nanyang Technological University, Singapore.

<https://hdl.handle.net/10356/36060>

<https://doi.org/10.32657/10356/36060>

Enhanced transverse wavelength tuning of fiber Bragg gratings for sensing applications

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A thesis submitted to the Nanyang Technological University in fulfillment of the requirement for the degree of Doctor of Philosophy

2006

ACKNOWLEDGEMENT

ACKNOWLEDGEMENT

I have been motivated, guided and accompanied by many people during the course of my Ph.D. I am deeply indebted to all of them for the successful completion of my Doctoral degree.

I feel a deep sense of gratitude to my supervisor Assoc. Prof. Murukeshan Vadakke Matham for his constant support and inspiration during the course of my Ph.D. I have been motivated and guided by him at many critical points and his expertise in the subject has been extremely useful at many stages. I express my sincere gratitude for his inspiring and thoughtful guidance and supervision.

I would like to express my sincere gratitude to Prof. Bryan Ngoi. I have been deeply inspired and motivated by his enthusiasm, integral view on research and his mission for providing opportunities for high quality research. I owe him lots of gratitude for having me shown this way of research. I am extremely thankful to him for his expert guidance, encouragement and support for the successful completion of the thesis.

I greatly indebted to the support and guidance obtained from my co-supervisor Dr. Zhao Liping from A-STAR Singapore Institute of Manufacturing Technology. She has given me wonderful support throughout the project and used to help me even in the minute aspects of the projects. Her cheerful and positive attitude has always been a constant source of encouragement.

I wish to thank the Agency for Science, Technology and Research (A-STAR) Singapore for the attachment program with SIMTech, one of its premier research institution, and giving opportunity to utilize its experimental facilities. I wish to thank Dr. Fang Zhong Ping, SIMTech for all his guidance throughout the project. I am extremely thankful to Dr. Erik Lee, SIMTech for rendering his expertise and help for FBG fabrication.

I express my sincere gratitude towards all the research staffs, research students and technicians in SIMTech, Metrology lab, PEN centre and NNC for their valuable

ACKNOWLEDGEMENT

support. Special thanks to the professors and researchers in School of MPE, EEE and SIMTech whose timely guidance was very much helpful for carrying out the project successfully.

I thank all my friends with whom I always shared my research experiences, thoughts and challenges. Their inspiration and support at difficult times and their valuable suggestions for the research activities helped a lot during the course of my Ph.D.

I feel a deep sense of gratitude for my father and mother who formed part of my vision and taught me the good things that really matter in life. Their persistent inspiration and prayers has always been a source of motivation for me. I am grateful to my brother who has supported and stood with me in all the endeavors of my life.

The God has given me wisdom and his grace is with me in all my activities. I bow my head before him for inspiring and guiding this humble being.

ABSTRACT

Fiber Bragg grating (FBG) based strain sensors have found a variety of applications in the last two decades. Most of these applications utilize the wavelength tuning of FBGs through an axially induced strain because of the high axial load sensitivity in comparison to lateral loading. However, wavelength tuning through lateral or transverse loading is attractive for applications like structural health monitoring, damage detection etc, owing to the flexibility in mounting or embedding the sensor. The wavelength tuning of the FBG sensors through lateral loading has the advantage of easy surface mounting, ruggedness, long term stability, structural integrity and more resistant to vibration. However, the potentials of wavelength tuning through lateral loading are yet to be utilized by overcoming its inherent limitations of low lateral pressure sensitivity, narrow tuning range and stress induced birefringence.

In order to understand these problems, various studies regarding the lateral loading process are performed initially. These studies include analysis of influence of the pressurizing media (or contact media) and contact conditions on the wavelength response characteristics of the FBGs. It is found that the material of the pressurizing media, its configuration and the contact friction play major roles in lateral pressure tuning (LPT) process and significant enhancement in performance can be achieved by optimizing these parameters. The lateral pressure sensitivity is high for a contact media with low value of Young's modulus. The sensitivity also increases with both the thickness of the contact plate and the contact friction between the fiber and the contact media. However, using an optimized contact media always may not be feasible in all working conditions.

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To address these shortcomings further and to provide a more convenient solution, polymer coating technique is being proposed. The dependence of peak wavelength shift sensitivity and birefringence on the coating thickness and stiffness of the polymer material is investigated. It is found that suitable coating material with low elastic modulus and high Poisson's ratio can effectively transfer the stress, applied in the lateral direction, to the perpendicular plane, thereby reducing the birefringence and enhancing the lateral pressure sensitivity. Through the analysis, it has been found that the peak wavelength shifts for both X and Y polarizations increase with increasing coating thickness. However, there exists an optimum range of coating thickness outside which the peak shift sensitivity decreases. Therefore a suitable polymeric coating with low stiffness and an optimum value of coating thickness is very effective in enhancing the wavelength shift sensitivity and in eliminating the stress-induced birefringence under lateral loading.

Cladding etching around the FBG combined with polymer coating is studied in order to evaluate its potential in enhancing the lateral load sensitivity without compromising the axial load and temperature sensitivities. This simple, inexpensive method proves to provide a compact design which simultaneously enhances the axial load, lateral load and temperature sensitivities without sacrificing the mechanical reliability. The same concept is also utilized for the design of an athermal FBG in which the wavelength shift is independent of temperature changes.

Reliability studies are performed to ensure the long life of the laterally loaded FBG since fiber and the attached pressure transmitting system have to undergo static and cyclic mechanical stresses for the purpose of wavelength tuning. Results of fatigue

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studies by finite element (FE) analysis are compared with the standard stress-life data available in the literature. For the optical fiber, maximum stress for fatigue failure is independent on the number of loading cycles and depends only on the magnitude of the applied stress where as for the polymeric coating and contact plate an intermittent stress or a periodic stress constitutes a more severe condition than a continuous stress of the same magnitude. The polymer parts are found to be more prone to failure than the FBG and therefore the lifetime of the polymer coating and the polymeric contact media can be considered as the lifetime of the sensor system. Also it was found that the typical tuning range achieved in LPT of FBGs requires loads well below the maximum safe limits for fatigue failure.

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LIST OF SYMBOLS

α_f - Thermal expansion coefficient of the optical fiber

α_c - Thermal expansion coefficient of the coating

α_{co} - Thermal expansion coefficient of the fiber core

α_{cl} - Thermal expansion coefficient of the cladding

α_{eff} - Effective thermal expansion coefficient

β - Propagation constant at wavelength λ

β_B - Propagation constant at wavelength λ_B

Δ - Gradient

δ - Gradient, deflection

ε_x - Strain in the X direction

ε_y - Strain in the Y direction

ε_z - Strain in the Z direction

ϕ - Chirp rate, angle

Γ - Core power confinement factor

γ_{xy} - Shear strain in the XY plane

γ_{xz} - Shear strain in the XZ plane

γ_{yz} - Shear strain in the YZ plane

Λ - Period of the FBG

Λ_B - Period of the FBG corresponding to λ_B

$\Lambda_{B,0}$ - Period of the undisturbed FBG

λ - Wavelength

λ_B - Bragg center wavelength

λ_{Bx} - Bragg center wavelength for X polarization

λ_{By} - Bragg center wavelength for Y polarization

μ - Coefficient of friction

ν_l - Poisson's ratio of optical fiber

ν_2 - Poisson's ratio of the contact media

ν_c - Poisson's ratio of the coating

LIST OF SYMBOLS

θ	- General symbol to represent angle
ρ	- Total reflected power
ρ_x	- Reflected power for X-polarization
ρ_y	- Reflected power for Y-polarization
σ_x	- Stress component in the X-direction
σ_y	- Stress component in the Y-direction
σ_z	- Stress component in the Z-direction
$\hat{\sigma}$	- Phase mismatching coefficient.
ν	- Fringe visibility
η	- Permittivity
ω	- Wave pulsation
ψ	- Adhesion factor
ζ_n	- Thermo optic coefficient of the optical fiber
$2a$	- Contact length
A_{co}	- Cross sectional area of fiber core
A_{cl}	- Cross sectional area of fiber cladding
A_c	- Cross sectional area of coating
A_{fl}	- Cross sectional area of the fiber after etching
A_j	- Amplitude of the forward propagating mode j
B_j	- Amplitude of the backward propagating mode j
B	- Birefringence
b	- Breadth of the contact plate
c	- Crack length
d	- Fiber diameter after etching
D	- Diameter of coated FBG
$\vec{E}$	- Electric field
$\vec{e}$	- Mode field
E_1	- Young's modulus of the optical fiber
E_2	- Young's modulus of the contact media
E_c	- Young's modulus of the coating material
F	- Force

LIST OF SYMBOLS

g -Effective index parameter
 I -Moment of inertia
 j, k -Symbols representing modes
 K_{jk}^t - Transverse coupling coefficient between modes j and k
 K_I -Stress intensity factor
 L -Length of the grating
 L_c -Length of coating
 m -order of diffraction
 n - Fatigue stress corrosion index
 N -Number of grating period, number of loading cycles
 n_{cl} -Cladding refractive index
 n_{co} -Core refractive index
 n_{eff} -Effective refractive index of the optical fiber
 $n_{eff,0}$ -Effective refractive index of the optical fiber at zero load
 P -Lateral load
 p - Pressure
 P_{11}, P_{12} -Photo elastic coefficients
 R -Fiber radius
 R_c -Radius of coated FBG
 r -Reflectivity
 r, θ - Co-ordinates in the polar co-ordinate system
 r_{co} -Core radius
 t -Time
 t - Thickness of the contact plate, coating thickness
 T -Temperature
 U - General term for displacement
 x, y, z -Location in the Cartesian co-ordinate system
 X, Y, Z -Perpendicular directions in the Cartesian co-ordinate
 Y -Parameter representing crack geometry

Chapter 1

INTRODUCTION

1.1 BACKGROUND

The fiber optic technology offers the unique benefit of a wide bandwidth, thereby allowing conveyance of a large amount of information through a single fiber. Fiber optics sensors are replacing conventional optical sensors in many areas in science, engineering, and medicine. Researchers are also creating new kinds of fiber sensors that have unique properties and can measure various physical, chemical, and biological phenomena [1].

While the discovery of photosensitivity in optical fibers in 1978 [2] was a major break through in the evolution of fiber optic systems, it gave rise to the development of a new class of in-fiber component called the fiber Bragg gratings. Bragg gratings inscribed in the core of an optical fiber are capable of performing many functions such as wavelength reflection and filtering, in a highly efficient manner independent of intensity. FBGs were already recognized as one of the key components in optical communications and sensing systems owing to the large number of device functions that they can perform. They are extensively used in wavelength division multiplexing (WDM), a technology for upgrading the capacity of fiber optic networks. [4]. They are capable of performing functions like wavelength add/drop, filtering, attenuation, pump stabilization and dispersion compensation [5,6]. They also find other uses in communication systems, for example, as gain equalizers, where their gain profile is designed to match the gain curve of an erbium doped fiber amplifier (EDFA) [7,8].

Even though competing technologies are available, the attraction of a FBG based devices lies in its flexibility to design, shape and control its output as per the application requirement.

The high sensitivity of grating wavelength shift to strain and temperature has made FBG to evolve as an excellent sensing device. The striking advantage of an FBG sensor is that the sensing parameter is wavelength encoded and therefore does not depend on light intensity levels or power loss. FBG sensors are currently being used for measuring a variety of parameters including strain [9-14], temperature [15-20] and humidity [21], and for numerous other sensing applications in the field of science, engineering and medicine [22-28].

The challenge in the design of an FBG based sensor lies in the effective transfer of the sensing parameter to cause a corresponding change in the grating parameters. This change in the grating parameters modulates the spectral response of the FBG which can be monitored through a suitable interrogation system. Wavelength tuning through axial loading, in which the principal strain direction coincides with the longitudinal axis of the fiber, is utilized for most of the sensing applications owing to its higher axial load sensitivity. In the case of wavelength tuning through lateral loading, the principal sensing force is made to act in the radial direction. Here the fiber containing the grating deforms in the radial direction along with insignificant axial strain and the deformation pattern depends on the loading geometry.

1.2 MOTIVATION

Most of the sensing applications make use of the wavelength tuning of FBGs through an axially induced strain because of the high axial load sensitivity in comparison to

lateral loading. However, wavelength tuning through lateral or transverse loading is attractive for applications like structural health monitoring, damage detection etc, owing to the flexibility in mounting or embedding the sensor. The wavelength tuning of the FBG through lateral loading has the advantage of easy surface mounting, robustness, long term stability, structural integrity and more resistant to vibration. Owing to the same reason, lateral tuning is the ideal option for vibration and dynamic strain measurements, especially in harsh environments. However, the full potentials of wavelength tuning through lateral loading are not yet fully explored owing to the low lateral pressure sensitivity.

The lateral pressure tuning process suffers also from the drawbacks of narrow tuning range and birefringence. In the case of lateral pressure tuning, contrary to axial and thermal tuning, the stress distribution in the two orthogonal polarization directions across the cross section of the bare fiber is very different, which leads to a stress-induced birefringence. This stress-induced birefringence produces two distinct Bragg wavelengths, which eventually leads to pulse broadening and peak splitting. Thus, the drawback with regard to peak splitting and low lateral pressure sensitivity has to be addressed in order to utilize the full potentials of FBG based strain sensors.

Again, for most of the advanced sensing applications like multidirectional strain sensing and multi parameter sensing, it is important to have high lateral load sensitivity along with significant axial load and temperature sensitivities. Thus the need arises also for a simple compact design which enhances the lateral load sensitivity without compromising the axial load and temperature sensitivities.

Moreover, for practical implementation of the device, it is important to ensure that the operating stresses and the loading frequencies are well below the critical level in order to guarantee the life of the component. Silica fiber is very fragile and highly

prone to fracture and therefore the wavelength tuning by transverse loading raises a serious question on the ability of the fiber to resist the induced stresses. Even though the reliability of FBGs subjected to axial loading has been a research interest [106-113], no work has been reported on the reliability upon transverse loading.

1.3 OBJECTIVES

The main objectives of the current research work are,

- 1) To enhance the performance of laterally loaded FBG sensors by addressing the problems of low lateral pressure sensitivity, narrow tuning range and birefringence.
- 2) To devise a simple compact design which enhances the lateral load sensitivity as well as the axial load and temperature sensitivities
- 3) To ensure that the required performance of the laterally loaded FBG sensors is achievable at stress levels and frequencies well below the critical limit for immature failure

1.4 SCOPE OF THE PROJECT

This doctoral work enables better utilization of wavelength tuning of FBGs for sensing applications. Accordingly, the scope of the project can be summarized as follows.

- Theoretical analysis and formulation of the influence of the pressurizing media (contact media) on the lateral pressure tuning of FBGs
- Optimization of the contact media configuration through finite element analysis for enhanced wavelength tuning supported by experimental analysis

- Theoretical analysis and formulation of the lateral pressure tuning of the proposed polymer coated FBG
- Finite element optimization of the design of polymer coated FBG for enhanced lateral pressure sensitivity and reduced birefringence
- Experimental analysis of the lateral pressure tuning of the optimized design polymer coated FBGs
- Theoretical formulation, finite element analysis and experimental analysis of the wavelength tuning of polymer coated FBG by virtue of hydrostatic pressure
- Utilization of cladding etching combined with polymer coating as a method to design athermal FBGs as well to enhance the wavelength tuning by axial, lateral, and thermal loading
- Theoretical studies of the mechanical reliability of the system during the lateral pressure tuning process
- Exploration of potential applications. This includes the conception and design of a novel fiber optic sensor for handgrip strength monitoring.

1.5 ORGANIZATION OF THE THESIS

In chapter 2, an overview of the state of the art of wavelength tuning of FBGs for sensing applications is carried out.

Chapter 3 describes in detail, the concept of wavelength tuning through lateral loading. Theory of mode coupling in lateral pressure tuning is presented. The current state of the art is reviewed in depth and the shortcomings are highlighted.

In chapter 4, the influence of the pressurizing media on the wavelength tuning process through lateral loading is evaluated. Theoretical analysis has been carried out for different contact configurations. The effect of stiffness of the pressurizing media, its thickness and the effect of contact friction on the lateral pressure tuning of FBGs is evaluated using finite element method. Experimental analysis is also incorporated.

Chapter 5 deals with the lateral pressure tuning of the proposed polymer coated FBG. The theoretical analysis is followed by simulation and experiments. Analysis of a special case of lateral pressure tuning - hydrostatic pressure tuning- in which the sensing force is acting in the radial direction uniformly around the circumference of the fiber is also incorporated in this chapter.

In chapter 6, cladding etching combined with polymer coating is proposed as a method to enhance the wavelength tuning by axial, lateral, and thermal loading. The proposed cladding etching and polymer coating technique has also been utilized to design an athermal FBG in which the wavelength response is independent of temperature changes.

In chapter 7, the mechanical reliability of the polymer packaged FBGs subjected to lateral loading is addressed.

Chapter 8 is devoted for the exploration of potential applications of wavelength tuning through lateral loading.

The thesis ends with chapter 9, which draws the conclusions and suggests recommendations for future work.

Chapter 2

FIBER BRAGG GRATINGS AS STRAIN SENSING DEVICE:

REVIEW

In this chapter, an overview of the concept of wavelength tuning of FBGs with regard to sensing applications is presented. The theory of mode coupling in FBGs is presented. Various publications related to wavelength tuning through axial and thermal loading are reviewed and the associated problems are highlighted. Current techniques for achieving enhanced axial load and temperature sensitivities are analysed and summarized. An overview of the athermalisation techniques currently used for eliminating the temperature dependent wavelength fluctuations is also presented in this chapter.

2.1 BACKGROUND

The first reported Bragg grating dates back to 1978 when Hill and co-workers launched light at 488 nm from an Argon-ion laser into a germanosilicate optical fiber [2]. In those experiments, the grating was formed by inducing periodic perturbations of the refractive index along its length through the standing-wave interference pattern built by counter propagating beams and the center wavelength equalled the laser wavelength. Because of the long interaction length, narrow-band Bragg reflection filters were formed with reflectivities approaching 100%. After that, in spite of the significant interest raised at the time, few studies were pursued until 1989 when Meltz et al. [29] reported that gratings could be written by two-beam holographic exposure of UV radiation at the side of the fiber. This method dramatically improved the writing efficiency because of the

single photo-ionisation process induced by UV light (in contrast with the Hill experiment, in which photo-ionisation resulted from a two-photon absorption process). Moreover this method allowed the possibility of producing gratings with an arbitrarily selected Bragg wavelength simply by adjusting the angle between the exposed beams with respect to the fiber axis. Although the free space holographic method (Meltz's et al. method) allowed great flexibility in fabrication of FBGs, a higher reproducibility was achieved by a method based on near contact exposure through a phase mask [30]. In this technique, the grating was photo written by interference of -1 and $+1$ diffraction orders with a period equal to half of that of the mask independent of the exposure wavelength. Since then activity in photo-induced Bragg grating has increased rapidly leading to the demonstration of many types of gratings including moire, chirped, superstructure and apodized, each having unique spectral features.

2.2 THEORY OF FIBER BRAGG GRATINGS

A fiber Bragg grating comprises of periodic modulation of the refractive index in the core of an optical fiber. These types of uniform fiber gratings, where the phase fronts are perpendicular to the fiber's longitudinal axis and with grating planes are spaced by constant period, are considered the fundamental building blocks for most Bragg grating structures. Light guided along the core of an optical fiber will be scattered by each grating plane. If the Bragg condition is not satisfied, the reflected light from each of the subsequent planes becomes progressively out of phase and eventually cancels out [31]. Additionally, light that is not coincident with the Bragg wavelength resonance will experience very weak reflection at each of the grating planes because of index mismatch-this reflection accumulates over the length of the grating. Where the Bragg

condition is satisfied, the contributions of reflected light from each grating plane add constructively in the backward direction to form a back-reflected peak with a center wavelength defined by the grating parameters.

Fiber phase gratings are produced by exposing an optical fiber to a spatially varying and period pattern of ultraviolet intensity. What results is a perturbation to the effective refractive index n_{eff} of the guided modes of interest, described by

$$\delta n_{eff}(z) = \overline{\delta n_{eff}}(z) \left\{ 1 + \nu \cos \left[\frac{2\pi}{\Lambda} z + \phi(z) \right] \right\} \quad (2.1)$$

where $\overline{\delta n_{eff}}$ is the “dc” index change spatially averaged over a grating period, ν is the fringe visibility of the index change, Λ is the nominal period, and $\phi(z)$ describes the grating chirp. If the fiber has a step index profile and an induced index change $\delta n_{co}(z)$ is created uniformly across the core, then we find that [32]

$$\delta n_{eff} \cong \Gamma \delta n_{co} \quad (2.2)$$

where Γ is the core power confinement factor for the mode of interest. Γ can be determined from

$$\Gamma = \frac{g^2}{V^2} \left[1 - \frac{J_l^2(V\sqrt{1-g})}{J_{l+1}(V\sqrt{1-g})J_{l-1}(V\sqrt{1-g})} \right] \quad (2.3)$$

Here g is the effective index parameter for LP_{lm} modes and is a solution to the relation

$$V\sqrt{1-g} \frac{J_{l-1}(V\sqrt{1-g})}{J_l(V\sqrt{1-g})} = -V\sqrt{g} \frac{K_{l-1}(V\sqrt{g})}{K_l(V\sqrt{g})} \quad (2.4)$$

where l is the azimuthal order of the mode and $V = (2\pi/\lambda)r_{co}\sqrt{n_{co}^2 - n_{cl}^2}$ is a normalized frequency, with r_{co} the core radius, n_{co} the core index, and n_{cl} the cladding index. The effective index is related to g through $g = (n_{eff}^2 - n_{cl}^2)/(n_{co}^2 - n_{cl}^2)$.

Thus the optical properties of fiber grating are essentially determined by the variation of the induced index change δn_{eff} along the fiber axis z .

2.2.1 Resonant wavelength for grating Diffraction

A fiber grating is simply an optical diffraction grating, and thus its effect upon a light wave incident on the grating at an angle θ_1 can be described by the famous grating equation

$$n \sin \theta_2 = n \sin \theta_1 + m\lambda/\Lambda \quad (2.5)$$

where θ_2 is the angle of the diffracted wave and the integer m determines the diffraction order.

Figure 2.1 Schematic showing the diffraction of light wave by a grating in a medium of refractive index n [32].

This equation predicts only the direction θ_2 into which constructive interference occurs, but it is nevertheless capable of determining the wavelength at which a fiber grating most efficiently couples light between two modes.

Fiber Bragg gratings can be broadly classified into two types: Bragg gratings (also called reflection and short period gratings) in which coupling occurs between

modes travelling in opposite direction; and transmission gratings (also called long period gratings), in which coupling is between modes travelling in same direction. Figure 2.2 illustrates reflection by a Bragg grating of a mode with a bounce angle of θ_1 into the same mode travelling in the opposite direction with a bounce angle of $\theta_2 = \theta_1$. Since the mode propagation constant β is simply $\beta = (2\pi/\lambda) n_{eff}$ where $n_{eff} = n_{co} \sin \theta$, we may write Equation 2.5 for guided modes as,

$$\beta_2 = \beta_1 + m2\pi/\Lambda \quad (2.6)$$

For first order diffraction, which usually dominates in a fiber Bragg grating $m = -1$. This condition is illustrated on the β axis shown below the fiber. The solid circles represents the bound core modes ($n_{cl} < n_{eff} < n_{co}$), the open circles represents the cladding modes ($1 < n_{eff} < n_{cl}$), and the hatched regions represents the continuum of radiation modes. Negative β values describe modes that propagate in the $-z$ direction. By using Equation 2.6 and recognising $\beta_2 < 0$, we find that resonant wavelength for reflection of a mode of index $n_{eff,1}$ into a mode of index $n_{eff,2}$ is

$$\lambda = (n_{eff,1} + n_{eff,2})\Lambda \quad (2.7)$$

If the two modes are identical, we get the familiar result for Bragg reflection:

$$\lambda = 2n_{eff}.\Lambda \quad (2.8)$$

Diffraction by a transmission grating of a mode with a bounce angle of θ_1 into a co-propagating mode with a bounce angle of θ_2 is illustrated in Figure 2.3. In this illustration, the first mode is a core mode where the second is a cladding mode. Since here $\beta_2 > 0$, Equation 2.6 predicts the resonant wavelength for a transmission grating as

$$\lambda = (n_{eff,1} + n_{eff,2})\Lambda \quad (2.9)$$

Thus for co-propagating coupling at a given wavelength, a much longer period Λ is required than for counter propagating coupling.

Figure 2.2 Ray optic illustration of core mode Bragg reflection by a FBG [32]

Figure 2.3 Ray optic illustration of cladding mode coupling of a FBG [32]

2.2.2 Fabrication of fiber Bragg Gratings

Bragg gratings may be classified as internally or externally written, depending on the fabrication technique employed. Although these fabrication processes were initially considered difficult due to the requirements of submicron resolution and thus stability, today they are well controlled and the inscription of FBGs using these techniques is considered routine [31].

Hill and co-workers first demonstrated the internal writing technique in 1978, using a single frequency laser light whose two-photon absorption lies in the UV photosensitivity region of the fiber which initiates the change in the index of refraction. [3]. This technique is simple and the experimental requirements are minimal. But these gratings are limited to operating at a Bragg wavelength coinciding with the excitation laser wavelength.

In 1989, Meltz and co-workers demonstrated the interferometric fabrication technique in which gratings can be written externally [42]. They utilized an interferometer that split the incoming UV light into two beams that were subsequently recombined to form an interference pattern that side-exposed a photosensitive fiber, inducing a permanent refractive index modulation in the core. Amplitude splitting [3] and wave front splitting interferometers have been used to fabricate fiber Bragg gratings [33].

Phase mask technique can be used to inscribe gratings effectively in a photosensitive fiber [30,34]. This method employs a diffractive optical element (the phase mask) to spatially modulate the UV writing beam. Phase masks may be formed either holographically or by electron beam lithographically. The phase mask is produced as a one-dimensional periodic surface relief pattern etched into fused silica.

The profile of the periodic surface relief gratings is chosen such that when a UV beam is incident on the phase mask, the zero order diffracted beam is suppressed to less than a few percent of the transmitted power. In addition the diffracted plus and minus first orders are maximized; each typically containing more than 35% of the transmitted power. A near field fringe pattern is produced by the interference of the plus and minus first order diffracted beams. A cylindrical lens may also be used to focus the fringe pattern along the fiber core. The period of the fringes is one half that of the mask. The interference pattern photo-imprints a refractive index modulation in the core of a photosensitive optical fiber that is placed in contact with or in close proximity to the phase mask.

Figure 2.4 Phase mask technique for inscribing Bragg grating [30]

2.2.3 Types of fiber Bragg gratings

There are a several distinct types of fiber Bragg grating structures depending upon the profile of refractive index modulation. In a uniform Bragg grating, the index modulation is uniform along the axis of the fiber and the grating plane is normal to the fiber axis. A tilt in the angle of the grating plane (called tilted or blazed Bragg grating) changes the coupling efficiency and bandwidth of the light that is tapped out [35].

A chirped grating has a monotonically varying period along the fiber axis. These have specific applications in telecommunications and sensor technology, such as dispersion compensation and the stable synthesis of multiple wavelength sources [36,37].

Figure 2.5. Some of the common types of Bragg gratings [35-40]: a) uniform b) phase shifted c) multiple period d) chirped e) apodised and f) superstructure

The reflectivity of the side lobes in the uniform FBG can be suppressed by apodization of the grating. Apodization can be done in a Gaussian profile or raised-cosine profile [38]. By introducing a phase shift across the fiber grating and by

adjusting its location and magnitude, it is possible to design a specific transmission spectrum. Phase shifts can be incorporated in the FBG [39]. These are called phase shifted Bragg gratings. A superstructure Bragg grating refers to a grating fiber structure fabricated with a modulated exposure over the length of the grating [40]. The refractive index profile along the fiber axis for various types of gratings is shown in Figure 2.5.

2.2.4 Principle of wavelength tuning of FBG

The center wavelength λ_B of a Bragg grating is given by [31] the condition

$$\lambda_B = 2 n_{eff} \cdot \Lambda \quad (2.10)$$

where n_{eff} is the effective refractive index of the fiber core at free space center wavelength and Λ is the spacing of the grating. The above equation can be differentiated to find the relative shift in Bragg center wavelength with respect to effective refractive index and grating period.

$$d\lambda_B = 2n_{eff} \cdot d\Lambda + 2\Lambda \cdot dn_{eff} \quad (2.11)$$

Dividing Equation 2.11 by Equation 2.10 gives the relative wavelength gradient.

$$\frac{d\lambda_B}{\lambda_B} = \frac{dn_{eff}}{n_{eff}} + \frac{d\Lambda}{\Lambda} \quad (2.12)$$

This means that the required shift in the center wavelength $\frac{d\lambda_B}{\lambda_B}$ can be achieved either by inducing a gradient in the effective refractive index of the grating $\frac{dn_{eff}}{n_{eff}}$ or by a change in the period of the grating $\frac{d\Lambda}{\Lambda}$ or both.

Thus tuning of a fiber Bragg grating can be defined as the process of controlling the spectral profile or output of the FBG by changing the grating parameters that influence the spectrum. Various techniques have been developed in the past few years to tune the center wavelength of an FBG so that it can be utilised for numerous applications in the area of sensing and telecommunications. The direct and simplest method is simply stretching/compressing the fiber containing the grating [41,42,43,44]. This gives an increment in the period of the grating leading to a shift in the center wavelength. Similarly, applying a lateral or hydrostatic pressure over the FBG can also change the period of the grating [87]. The concept has been utilised for the design of a variety of strain and pressure sensors. A change in the ambient temperature can also cause a gradient in both the effective refractive index and the period of the grating due to the temperature dependence of the refractive index and elastic properties of the fiber. This is the basic concept behind all FBG based temperature sensing devices and temperature tunable filters [11-20].

The shift in Bragg grating center wavelength due to strain and temperature gradient can be summarized as [31]

$$\Delta\lambda_B = 2\left(\Lambda \frac{\partial n_{eff}}{\partial l} + n_{eff} \frac{\partial \Lambda}{\partial l}\right)\Delta l + 2\left(\Lambda \frac{\partial n_{eff}}{\partial T} + n_{eff} \frac{\partial \Lambda}{\partial T}\right)\Delta T \quad (2.13)$$

The first part of the equation represents the strain effects on an optical fiber. This

corresponds to a change in grating spacing $\frac{\partial \Lambda}{\partial l}$ and the strain-optic induced change $\frac{\partial n_{eff}}{\partial l}$ in the refractive index. (Δl) indicates the change in the grating length and (ΔT) , the change in temperature. The second term represents the effect of temperature on an optical fiber.

The strain dependence of the Bragg center wavelength can be formulated as [31]

$$\frac{\Delta\lambda_B}{\lambda_B} = \varepsilon_z - \frac{n_{co}^2}{2} [\varepsilon_r (P_{11} + P_{12}) + \varepsilon_z P_{12}] \quad (2.14)$$

where $\frac{\Delta\lambda_B}{\lambda_B}$ is the relative shift in the center wavelength, ε_z is the axial strain, ε_r is the radial strain, n_{co} is the refractive index of the fiber core, and P_{11} and P_{12} are the photoelastic coefficients of silica in the axial and radial directions respectively. For an isotropic fiber, the radial strain can be written in terms of axial strain using Poisson's law as

$$\varepsilon_r = -\nu \varepsilon_z \quad (2.15)$$

where ν is the Poisson's ratio of the fiber material. Taking the values of photoelastic coefficients as $P_{11}=0.113$ and $P_{12}=0.252$, refractive index $n_{co} = 1.51$ and $\nu=0.16$, the relative wavelength shift

$$\frac{\Delta\lambda_B}{\lambda_B} = 0.78 \varepsilon_z \quad (2.16)$$

The second term in Equation 2.13 represents the effects of temperature on an optical fiber. A shift in Bragg wavelength due to thermal expansion changes the grating spacing and the index of refraction. The fractional wavelength shift for a temperature change ΔT may be written as [44]

$$\Delta\lambda_B = \lambda_B (\alpha_\Lambda + \alpha_n) \Delta T \quad (2.17)$$

where $\alpha_\Lambda = \frac{1}{\Lambda} \frac{\partial \Lambda}{\partial T}$ is the thermal expansion coefficient of the fiber and

$\alpha_n = \frac{1}{n_{eff}} \frac{\partial n_{eff}}{\partial T}$ represents the thermo-optic coefficient for the germania-doped, silica-core fiber.

The typical values of α_Λ and α_n for germania doped, silica core fiber is given as [31],

$$\alpha_\Lambda = 5.6 \times 10^{-7}$$

$$\alpha_n = 8.6 \times 10^{-6}$$

2.3 THEORETICAL MODEL FOR FIBER BRAGG GRATINGS

Even though techniques like the transfer matrix method [45], Rourd's technique [46], and Floquet-Bloch formalism [47,48], have been utilized to model the properties of FBGs, the most commonly used one is coupled mode theory. This method assumes that the mode fields of the unperturbed wave-guide remain unchanged in the presence of weak perturbation. This approach provides a set of first order differential equations for the change in the amplitude of the fields along the fiber. Analytical solutions for this can be obtained for any periodic perturbations.

2.3.1 Coupled mode theory

The concept of coupled modes in electromagnetics may be traced back to the early 1950's. The application was initially to microwaves and developed gradually through the contributions of many people. The early approach to coupled mode theory was rather heuristic [49]. The modes of the "uncoupled systems" were identified and the coupled mode equations obeyed by the mode amplitudes were determined from power considerations.

The coupled mode theory for optical wave-guides was developed in the early 1970's [50,51,52]. Since then, it has been successfully applied to the modelling and analysis of various guided wave opto-electronic and fiber optical devices.

An assumption made in conventional coupled mode theory is that the modes of the uncoupled systems are orthogonal to each other. This may be true if the modes belong to the same reference structures [49]. In studying the mode coupling in coupled systems, one often chooses the modes of the isolated systems as the basis for the mode expansion and these modes may not be orthogonal. The orthogonal coupled mode theory is not correct for the description of the mode coupling process in this case.

In ideal mode approximation to CMT, it is assumed that the transverse component of electric field $\vec{E}_t(x, y, z, t)$ can be written as a superposition of the ideal modes labelled j (i.e., the modes in an ideal waveguide with no perturbation) [31], such that

$$\vec{E}_t(x, y, z, t) = \sum_j [A_j(z) \exp(i\beta_j z) + B_j(z) \exp(-i\beta_j z)] \vec{e}_{jt}(x, y) \exp(-i\omega t) \quad (2.18)$$

where $A_j(z)$ and $B_j(z)$ are slowly varying amplitudes of the j^{th} mode travelling in the $+z$ and $-z$ directions, respectively. The transverse mode fields $\vec{e}_{jt}(x, y)$ might describe the bound core or radiation LP modes or cladding modes. While modes are orthogonal in an ideal waveguide and hence, do not exchange energy, the presence of a dielectric perturbation causes the modes to be coupled such that the amplitudes A_j and B_j of the j^{th} mode evolve along the z -axis according to [31]

$$\frac{dA_j}{dz} = i \sum_k A_k (K_{kj}^t + K_{kj}^z) \exp[i(\beta_k - \beta_j)z] + i \sum_k B_k (K_{kj}^t - K_{kj}^z) \exp[-i(\beta_k + \beta_j)z] \quad (2.19)$$

$$\frac{dB_j}{dz} = -i \sum_k A_k (K_{kj}^t - K_{kj}^z) \exp[i(\beta_k + \beta_j)z] - i \sum_k B_k (K_{kj}^t + K_{kj}^z) \exp[-i(\beta_k - \beta_j)z] \quad (2.20)$$

In Equations 2.19 and 2.20, $K_{kj}^t(z)$ is the transverse coupling coefficient between modes j and k given by

$$K_{kj}^t(z) = \frac{\omega}{4} \iint_{\infty} dx dy \Delta\eta(x, y, z) \vec{e}_{k,t}(x, y) \cdot \vec{e}_{j,t}(x, y) \quad (2.21)$$

where $\Delta\eta$ is the perturbation to the permittivity, approximately $\Delta\eta \cong 2n\delta n$ when $\delta n \ll n$. The longitudinal coefficient $K_{kj}^z(z)$ is analogous to $K_{kj}^t(z)$, but generally $K_{kj}^z(z) \ll K_{kj}^t(z)$ for fiber modes, and thus this coefficient is usually neglected.

In most fiber gratings, the induced index change $\delta n(x, y, z)$ is approximately uniform across the core and non-existent outside the core. Thus the core index can be described by the expression

$$\delta n_{co}(z) = \overline{\delta n_{co}}(z) \left\{ 1 + \nu \cos \left[\frac{2\pi}{\Lambda} z + \phi(z) \right] \right\} \quad (2.22)$$

where $\overline{\delta n_{co}}$ is the “dc” index change spatially averaged over a grating period, ν is the fringe visibility of the index change, Λ is the nominal period, and $\phi(z)$ describes the grating chirp.

The coupling coefficients can be written as

$$\sigma_{kj}(z) = \frac{\omega n_{co}}{2} \overline{\delta n_{co}}(z) \iint_{core} dx dy \vec{e}_{k,t}(x, y) \cdot \vec{e}_{j,t}(x, y) \quad (2.23)$$

$$k_{kj}(z) = \frac{\nu}{2} \sigma_{kj}(z) \quad (2.24)$$

where σ is a “dc” (period averaged) coupling coefficient and k is an “AC” coupling coefficient. The general coupling coefficient can be written as

$$K'_{kj}(z) = \sigma_{kj}(z) + 2k_{kj}(z) \cos \left[\frac{2\pi}{\Lambda} z + \phi(z) \right] \quad (2.25)$$

These equations can be used to derive the fiber grating spectral response. Reflection of a mode of amplitude $A(z)$ into an identical counter propagating mode of amplitude $B(z)$ is the dominant interaction in the Bragg grating. Equations 2.19 and 2.20 may be simplified by retaining only terms that involve the amplitudes for the particular mode, and then making the “synchronous approximation” [53].

This amounts to neglecting terms on the right hand side of the differential equations that contain rapidly oscillating z dependence, since these contribute little to the growth and decay of the amplitudes. The resulting equations can be written [53,31,32]

$$\frac{dR}{dz} = i\hat{\sigma}R(z) + ikS(z) \quad (2.26)$$

$$\frac{dS}{dz} = -i\hat{\sigma}S(z) - ik^*R(z) \quad (2.27)$$

where the amplitudes R and S are

$$R(z) \equiv A(z) \exp(i\delta z - \phi/2) \quad (2.28)$$

$$S(z) \equiv B(z) \exp(-i\delta z + \phi/2) \quad (2.29)$$

In these equations, k is the “AC” coupling coefficient from Equation 2.21 and $\hat{\sigma}$ is a general “dc” coupling coefficient defined as

$$\hat{\sigma} \equiv \delta + \frac{2\pi}{\lambda} \overline{\delta n_{eff}} - \frac{1}{2} \frac{d\phi}{dz} \quad (2.30)$$

The detuning parameter δ , which is independent of z for all gratings, is defined as

$$\delta \equiv \beta - \frac{\pi}{\Lambda} = \beta - \beta_B = 2\pi n_{eff} \left(\frac{1}{\lambda} - \frac{1}{\lambda_B} \right) \quad (2.31)$$

where $\lambda_B \equiv 2\pi\Lambda$ is the “design wavelength” for Bragg scattering by an infinitesimally weak grating ($\delta n_{eff} \rightarrow 0$) with a period Λ . When $\delta = 0$, $\lambda = 2n_{eff}\Lambda$. The “dc” coupling coefficient σ is defined in Equation 2.23.

The derivative $\frac{1}{2} \frac{d\phi}{dz}$ describes the possible chirp of the grating period. For a single mode Bragg reflection grating, the coupling coefficients can be written as

$$k = k^* = \frac{\pi}{\lambda} \nu \overline{\delta n_{eff}} \quad (2.32)$$

2.3.2 Reflectivity of a Bragg grating

If the grating is uniform along z , then $\overline{\delta n_{eff}}$ is a constant and $\frac{d\phi}{dz} = 0$, and thus k , σ and $\hat{\sigma}$ are constants. Thus the reflectivity of a uniform fiber grating of length L can be found by assuming a forward-going wave incident from $z = -\infty$ [say $R(-L/2)=1$] and requiring that no back-ward going wave exists for $z \geq L/2$ [i.e., $S(L/2)=0$]. The amplitude and power reflection coefficients $\rho = \frac{S(-L/2)}{R(-L/2)}$ and $r = |\rho|^2$, respectively can then be shown to be [53,31,32]

$$\rho = \frac{-k \sinh(\sqrt{k^2 - \hat{\sigma}^2} L)}{\hat{\sigma} \sinh(\sqrt{k^2 - \hat{\sigma}^2} L) + i\sqrt{k^2 - \hat{\sigma}^2} \cosh(\sqrt{k^2 - \hat{\sigma}^2} L)} \quad (2.33)$$

$$r = \frac{\sinh^2(\sqrt{k^2 - \hat{\sigma}^2} L)}{\cosh^2(\sqrt{k^2 - \hat{\sigma}^2} L) - \frac{\hat{\sigma}^2}{k^2}} \quad (2.34)$$

The normalised wavelength can be written as

$$\frac{\lambda}{\lambda_{\max}} = \frac{1}{1 + \frac{\hat{\sigma}L}{\pi N}} \quad (2.35)$$

where N is the total number of grating periods and $N=L/\Lambda$ and $\lambda_{\max}$ is the wavelength at which maximum reflectivity occurs. Depending upon whether N is larger or smaller, the reflection bandwidth would be narrower or broader, respectively, for a given value of kL . From Equation 2.34, the maximum reflectivity of a Bragg grating is

$$r_{\max} = \tanh^2(kL) \quad (2.36)$$

And it occurs when $\hat{\sigma} = 0$, or at the wavelength

$$\lambda_{\max} = \left(1 + \frac{\overline{\delta n_{\text{eff}}}}{n_{\text{eff}}}\right) \lambda_B \quad (2.37)$$

2.3.3 Expression for Band Width

The points at the edges of the “band gap” defined as $|\hat{\sigma}| < k$ can be called as the “band edges” and the reflectivity at the band edges is [53,31,32]

$$r_{\text{band edge}} = \frac{(KL)^2}{1 + (KL)^2} \quad (2.38)$$

and the band edges occur at wavelengths

$$\lambda_{\text{band edge}} = \lambda_{\max} \pm \frac{\nu \overline{\delta n_{\text{eff}}}}{n_{\text{eff}}} \quad (2.39)$$

Inside the band gap, the amplitudes $R(z)$ and $S(z)$ grow and decay exponentially along z direction and outside the band gap, they evolve sinusoidally. The normalised bandwidth as measured at the band edges is

$$\frac{\Delta\lambda_{band\ edge}}{\lambda} = \frac{\nu\overline{\delta n_{eff}}}{n_{eff}} \quad (2.40)$$

A more readily measurable bandwidth for a uniform Bragg grating is that between the first zeros on either sides of the maximum reflectivity [53,31,32]

$$\frac{\Delta\lambda_0}{\lambda} = \frac{\nu\overline{\delta n_{eff}}}{n_{eff}} \sqrt{1 + \left(\frac{\lambda_B}{\nu\overline{\delta n_{eff}} L} \right)^2} \quad (2.41)$$

In the “weak grating limit” for which $\nu\overline{\delta n_{eff}}$ is very small,

$$\frac{\Delta\lambda_0}{\lambda} \rightarrow \frac{\lambda_B}{n_{eff} L} = \frac{2}{N} \quad \left(\nu n_{eff} << \frac{\lambda_B}{L} \right) \quad (2.42)$$

Here the bandwidth of weak gratings is said to be “length limited”. For strong gratings,

$$\frac{\Delta\lambda_0}{\lambda} \rightarrow \frac{\nu\overline{\delta n_{eff}}}{n_{eff}} \quad \left(\nu n_{eff} >> \frac{\lambda_B}{L} \right) \quad (2.43)$$

If the refractive index modulation is very strong, the whole light will reflect before reaching the full length of the grating, and so the bandwidth is independent of length and directly proportional to the induced index change. Also, for strong gratings, the bandwidth is similar whether measured at the band edges, at first zeros, or as the full width at half maximum (FWHM).

2.4 WAVELENGTH TUNING OF FBGS: APPLICATION TO SENSORS

2.4.1 Pressure sensing through fiber optic means: Background

Sensors based on optical fibers was a research interest even much before the invention of fiber Bragg gratings. Influence of various types of pressures (e.g. acoustic,

mechanical) and strains has been extensively used to develop sensors to sense and measure these parameters.

When a sound wave propagates through a medium, the periodic pressure variations give rise to corresponding variations in the medium's index of refraction. Thus, the presence of sound can be detected by an optical interferometer. A change in the optical index of refraction of the fiber creates an effective path-length change for the optical beam which results in a phase shift of the optical beam with respect to a reference beam unaltered by the acoustic field. By mixing the phase-shifted beam with a reference beam of constant phase in a photodetector the phase variation at the acoustic frequency can be detected [54]. The concept has been used to develop fiber optic acoustic sensor. A fiber optic hydrophone is an example [55]. However, to produce a measurable phase shift, a high acoustic pressure is usually required because the acousto-optic coupling coefficient is weak.

However, later it was proven that the changes in length actually contributes more to hydrophone sensitivity than do the refractive index changes and the two contributions are of opposite signs [56]. Thus, the availability of long, low-loss optical fibers suggests the possibility of substantially increasing the interaction length, as high as few kilometers, by confining the optical beam to a fiber coil immersed in the fluid. This would increase the sensitivity of acoustic detection to the point where such an effect could be utilized to obtain a sensitive, optical acoustic sensor [57].

Later, this concept led to the development of fiber optic strain gauge [58] in which the strain induced optical path length difference of the two arms of the Mach-Zehnder interferometer. However it was found that the sensitivity of the fiber interferometer to static pressure changes seems low compared with other techniques, and the fringe motion due to pressure changes may often be much less than small

motions due to temperature changes, which led to the development of simultaneous sensing of strain and temperature [59]. Also it was reported that an ITT coated fiber has an order of magnitude increase in sensitivity over the bare glass fiber and it may be possible to coat with materials which decrease the sensitivity to acoustic waves for specific regions of the fiber if so desired [60].

So many efforts were reported to enhance the acoustic pressure sensitivity of the fiber optic sensor. Fiber optic acoustic sensors with composite structures were analyzed and shown to offer greatly increased acoustic sensitivity. The composite structure consists of an optical fiber coated with or embedded in an elastic material of lower elastic modulus. Sensitivity increases of 10-100 times were indicated [61].

Experiments carried out by Bucaro *et al.* demonstrate that an optical-fiber acoustic wave detector with a plastic coating several times its diameter exhibits greatly increased sensitivity compared with an uncoated fiber. It was shown that this effect should be expected because the plastic is much more compressible than the glass fiber. With a very thick coating of Teflon like plastic, the calculated longitudinal strain produced in the fiber by hydrostatic pressure was thirteen times larger than for an uncoated fiber. The effective phase change was increased by the much larger factor of 38 because the coated fiber expands laterally instead of contracting [62]. In a similar study, it was shown that the hydrophone sensitivity of a jacketed fiber is amplified compared with a bare fiber. The largest increase in sensitivity is predicted with the radial mode.

Minimizing the pressure sensitivity of optical fibers is important when fibers are used as sensors. In fiber optic acoustic sensors, one wants to localize the fiber sensitivity by making the lead fibers pressure insensitive. In other optical fiber type sensors, e.g., magnetic, optical gyros, temperature, one wants to desensitize the fiber to

acoustic waves which would otherwise act as a noise source. Various studies were concentrated in this direction also. A related analysis shows that there are combinations of glass and coating materials and corresponding thicknesses which make fibers pressure insensitive [63]. It was analytically demonstrated that an optical fiber can be made pressure insensitive by surrounding the glass core-clad with a high bulk modulus glass substrate or metal coating. The core-clad glass can have a conventional high silica composition or a relatively low bulk modulus non-conventional composition. For fibers having a high silica content core, the substrate glass must have a low expansion coefficient in addition to high bulk modulus [63]

It was also reported that [64, 65] with fibers having thick coatings, the acoustic and temperature sensitivity is dependent on its material properties and is inversely proportional to the bulk modulus of the coatings. For fibers with typical coating thicknesses, the fiber sensitivity is governed by both the Young's modulus and the bulk modulus of the fiber coatings. The temperature dependence of the acoustic sensitivity of fibers coated with different elastomers was found to vary considerably. Fibers with nylon coating have the weakest temperature dependent sensitivity, while fibers with the UV curable elastomers have the strongest [64, 65].

However, by the invention of fiber Bragg gratings [2] with extremely high sensitivity and lot of other promising features which can be made use for sensing applications, pressure and strain sensing using conventional fiber optic routes has become less important.

2.4.2 Sensors based on wavelength tuning of FBGs through axial loading

As seen from the analysis in section 2.2.4, an induced strain can cause a shift in the spectral response of a fiber Bragg grating. This property of FBGs has extensively been utilized for a variety of sensing applications. The sensing parameter is aptly converted into a force, which acts on the FBG in the longitudinal direction. This axial force strains the FBG and induces a gradient in the period of the grating, which produces a shift in its center wavelength. This wavelength response can be interrogated using a suitable system. Once this wavelength shift is calibrated in terms of the sensing parameter, it is possible to assess the sensing parameter from the sensor response.

Since the plane of the gratings is perpendicular to the longitudinal axis of the fiber, a loading configuration in which the principal loading direction coincides with the axis of the fiber produces a maximum gradient in the period of the grating. Because of this reason, wavelength tuning through axial loading is the most ideal option for encoding the particulars of the sensing parameter in terms of wavelength of the light.

Currently, the FBG sensors which utilize the wavelength tuning through axial loading includes pressure transducers [9, 73], various strain rosettes [26] and tilt angle sensor [27]. The concept is also being utilized for measuring displacement [9], dynamic strain [10], acceleration [22], inclination [23], fluid flow [24] and shear force [25].

2.4.3 Sensors based on wavelength tuning through thermal loading

Similar to the case of axial loading, a load develops a uniform refractive perturbation and stress distribution about the axis at the cross section of the fiber. This leads to linear shift in the center wavelength of the FBG with respect to temperature, without any

pulse broadening and peak splitting. Therefore the problem of birefringence does not arise in the case of thermal tuning. The temperature sensitivity of a bare FBG lies in the range 0.011-0.014 nm/°C depending on the type of the fiber.

The temperature sensitivity of FBGs can be utilized for applications like temperature sensor [15], distributed temperature sensing [16], measuring heat flow [17], measuring temperature in harsh nuclear environments [18] and high temperature measurements [19] and temperature measurement in medical applications [20].

Utilizing the strain and temperature sensitivities of FBG, simultaneous strain and temperature measurement is another common application of FBG sensors. In addition to uniform FBGs written on single mode low bi fiber, special gratings and combinations like polymer-coated fibre Bragg grating [73], Hi-Bi fiber Bragg gratings [12], superstructure fiber Bragg grating [13] and sampled fibre Bragg grating [14], has also been utilized for this purpose.

2.4.4 Techniques for enhanced sensitivities

By reducing the cladding diameter of FBG, the tension tuning force can be lowered by over an order of magnitude. Reflection wavelength can be shifted either by controlling the effective refractive index of the waveguide [54] or by stretching the grating mechanically [67, 68]. Wavelength tuning by bending control of asymmetric single mode grating fiber fabricated by anisotropic plasma etching has been proposed by Kumazaki et al. [69]. This greatly reduced force requirement offers the advantage of low power consumption but the strength of the fiber will be greatly reduced by etching of the cladding, which in turn reduces the extent of tuning.

Figure 2. 6 Schematic representing the force tuning of cladding etched fiber [66]

Bell Laboratories demonstrated a novel configuration to amplify thermally induced wavelength shifts by use of a negative thermal-expansion component [70]. The enhancement in temperature sensitivity of wavelength (for a given package length) in this design arises from the use of a unique metallic alloy with a negative coefficient of thermal expansion (CTE). One end of the FBG is bonded to a negative CTE Ti-Ni alloy, the other end, to a positive CTE aluminium alloy as shown in Fig. 2.7. When it is heated, the negative CTE component contracts, where as the positive CTE component expands, thereby inducing a superimposed temperature dependent tensile strain in the FBG, resulting in a wavelength shift that is programmable by temperature. This enhanced thermal tuning produces an essentially linear wavelength shift with temperature, with $\Delta\lambda \approx 4 \text{ nm}$, for a temperature rise $\Delta T=40 \text{ }^{\circ}\text{C}$.

Figure 2.7 Schematic of an enhanced thermal tuning device with a negative CTE component [70]

Figure 2.8 Schematic of a polymer coated FBG for enhanced thermal tuning [73]

Figure 2.9 Schematic showing the thermal tuning of a polymer FBG [74]

Thermal response of FBGs can also be improved either by coating the fiber with polymers having high thermal coefficients [71, 72, 73] as shown in Figure 2.8 or by using a FBG fabricated on a polymer itself as illustrated in Figure 2.9 [74]. Though FBG fabricated on polymer fibers possess the advantage of higher thermal sensitivity, they have very poor reflectivity and cannot be used for practical sensing applications.

2.5 ATHERMALISATION TECHNIQUES

Despite the thermal effect on FBGs being utilized for various thermally tuned Bragg grating devices and temperature sensors, applications like non-temperature sensors and

optical filters require elimination of the temperature dependent wavelength fluctuations. The methods that are currently used for providing this temperature compensation include active compensation techniques, which utilize continuous monitoring and dynamic control of certain parameters, and passive devices that utilize the thermal characteristics of materials to control the sensitivity of the FBG wavelength to temperature [75-83].

The passive compensation is more common and preferred owing to its uncomplicated nature and self-activation. Passive compensation devices control the elongation with temperature of the optical fiber containing the FBG. The fiber is clamped to a mechanical structure that imposes upon it a negative elongation as the temperature increases. This contraction of the fiber compensates for the increase in its index of refraction with temperature, thus allowing a stabilization of its Bragg wavelength against temperature fluctuations. The direct way to achieve this type of passive temperature compensation is by attaching the FBG to a substrate having suitable value of negative thermal expansion coefficient as typically demonstrated in [75]. Here the wavelength shift due to temperature changes is eliminated by attaching the grating to a support member comprising of Zr-tungstate and/or Hf-tungstate having a suitable value of negative coefficient of thermal expansion. However, this approach suffers from some major drawbacks. The coefficient of thermal expansion (CTE) must be accurately harmonized to the optical fiber properties, which requires very careful control of the integration of desired material properties.

Another conventional approach for generating a negative elongation during a temperature increase is by designing a mechanical structure, which consists of more than one material having different, but usually positive values of thermal expansion coefficient. The structure is arranged such that different rates of expansion between the

structural members supporting the fiber result in a negative elongation of the fiber with increasing temperature [76-83]. A typical example of this method is one proposed by Morey and Glomb [76] where each end of the fiber portion is attached to two compensating members made of different materials. The differential thermal expansion between these members provides an axial elongation along the fiber, which can accurately compensate the wavelength fluctuations due to temperature change. Fig. 2.10 shows an arrangement for temperature compensation based on the same concept [81]. It consists of two materials such that the coefficient of linear expansion of A is less than that of B. Another geometry is shown in Fig. 2.11 in which a low expansion silica tube combined with an aluminium tube was used to achieve the required compensation [78]. In the case of an increase in temperature, the aluminium components expand so that the distance between the mounting points decreases. This reduces the tension in the fiber by just the amount necessary to cancel the shift in Bragg wavelength that is due to temperature rise.

Figure 2.10 Schematic showing the set up for temperature compensation using a bimaterial device [81]

Figure 2.11. Schematic of set up for temperature compensation using low expansion silica tube combined with an aluminium tube [78]

In general, the passive temperature compensation set-up, constituting materials with different temperature coefficients of expansion, is bulky. Moreover, the technique

does not offer the flexibility of tailoring the physical dimensions of the structure according to the application of the device.

2.6 ADVANTAGES OF BRAGG GRATING SENSORS

The advantages of FBG sensors can be summarized as,

- Unlike other fiber optic sensors, the sensing parameter is wavelength encoded and therefore not dependent on the light levels or power loss
- Immune to electromagnetic interference. Because optical fiber is a dielectric, it is not subject to interference from electromagnetic waves that might be present in the sensing environment
- Low fiber loss with transmission capacity over several kilometers
- Light weight and small physical dimensions
- Suitable for being embedded into or attached to a structure or object to be tested
- Environmentally more stable and durable and free from rust and corrosion.
- Can be multiplexed and many sensing points in a single fiber cable is possible
- Can function under adverse conditions of temperature and pressure, and toxic or explosive atmospheres

2.7 SUMMARY AND CONCLUDING REMARKS

A comprehensive study of the theory of wavelength tuning and mode coupling in FBGs was presented. A review of the utilization of the wavelength tuning through axial and thermal loading for sensing application was presented and the current techniques for

achieving enhanced axial load and temperature sensitivity were reviewed. Various athermalisation techniques eliminate the temperature dependent wavelength fluctuations during non-temperature parametric sensing were also presented in this chapter.

The current methods for achieving enhanced sensitivities are associated with bulky mechanical structures. Moreover, no effort has reportedly been taken to incorporate the strain and temperature enhancement techniques in a single design. Therefore the need for a unique compact system, which has high axial, lateral and temperature sensitivities simultaneously, has to be highlighted. Moreover, a simple and compact design for athermalisation is essential for most of the advanced applications.

It could also be noted that most of the published works are concentrated on utilizing the wavelength tuning through axial loading despite the advantages offered by the lateral pressure tuning. In this situation, due effort have to be taken to understand the concept LPT of FBGs in depth and to analyze the reasons by which it remains unexplored.

Chapter 3

WAVELENGTH TUNING OF FBGs THROUGH LATERAL LOADING: REVIEW

A comprehensive study is necessary to understand the lateral pressure tuning (LPT) process and the reasons by which it remains unexplored despite of its explicit advantages for sensing applications. In this chapter, the concept and theory of lateral pressure tuning of FBGs is reviewed in detail. The theoretical formulation for stress induced refractive index modulation and the associated wavelength shift of FBGs subjected to LPT are presented. The polarization and birefringence by virtue of lateral loading and the formulation for mode coupling are also incorporated. A detailed overview of the current state of the art of wavelength tuning through lateral loading is carried out and the shortcomings are highlighted.

3.1 THEORY

Consider the FBG subjected to a lateral load P as shown in Figure 3.1. We assume that the optical fiber is cylindrical and isotropic with axially symmetric refractive index distribution in the absence of the external disturbance.

The stress distribution along the X-axis and Y-axis at any point (x, y) for a lateral load P acting over a length L is given by [84, 85, 86]

$$\sigma_x(x, y) = \frac{-2P}{\pi L} \left\{ \frac{x^2(R+y)}{[x^2 + (R+y)^2]^2} + \frac{x^2(R-y)}{[x^2 + (R-y)^2]^2} - \frac{1}{2R} \right\} \quad (3.1)$$

$$\sigma_y(x, y) = \frac{-2P}{\pi L} \left\{ \frac{(R+y)^3}{[x^2 + (R+y)^2]^2} + \frac{(R-y)^3}{[x^2 + (R-y)^2]^2} - \frac{1}{2R} \right\} \quad (3.2)$$

where R is the radius of the fiber and $0 \leq |x| \leq R$, $0 \leq |y| \leq R$.

Due to this applied stress field, the refractive index of the optical fiber changes by virtue of photoelastic effect.

Figure 3.1. Schematic of FBG subjected to lateral load P . d is the fiber diameter, n_1 is the core refractive index and n_2 is the cladding refractive index

A plot of the stress distribution σ_x and σ_y according to equations 3.1 and 3.2 is shown in figures 3.1a and 3.1b respectively. A lateral load of 5 N is assumed to be acting over a FBG length of 10 mm. The fiber diameter is 125 μm . The Cartesian co-ordinates (x, y) has been converted to polar co-ordinates $(r\cos\theta, r\sin\theta)$. The value of σ_x is maximum at an angle of 90° as can be seen from Fig. 3.1a. For other angles 0° and 45° , the stress

distribution follows a sinusoidal path, the maximum of which is centered at the axis ($r=0$) of the fiber. The negative values of σ_y in Fig. 3.1b indicate that the stresses are compressive in nature. Also it can be seen that the stresses are concentrated at the fiber core.

Fig. 3.1 a. A plot of the stress distribution σ_x according to equation 3.1 for a lateral load of 5 N acting over a FBG length of 10 mm.

Fig. 3.1 b. A plot of the stress distribution σ_y according to equation 3.2 for a lateral load of 5 N acting over a FBG length of 10 mm.

3.1.1 Refractive index modulation

The refractive index gradient at any point $P(x, y, z)$ in the grating is related to the stress distribution by the following equations [84, 85, 86]

for X polarization,

$$(\Delta n_{eff})_x(x, y, z) = -\frac{(n_{eff,0})^3}{2E} \times \{(P_{11} - 2\nu P_{12})\sigma_x(x, y, z) + [(1 - \nu)P_{12} - \nu P_{11}] \times [\sigma_y(x, y, z) + \sigma_z(x, y, z)]\} \quad (3.3)$$

for Y polarization,

$$(\Delta n_{eff})_y(x, y, z) = -\frac{(n_{eff,0})^3}{2E} \times \{(P_{11} - 2\nu P_{12})\sigma_y(x, y, z) + [(1 - \nu)P_{12} - \nu P_{11}] \times [\sigma_x(x, y, z) + \sigma_z(x, y, z)]\} \quad (3.4)$$

The refractive index gradient at any point (x, y, z) in the grating is related to the strain distribution by the equation [84, 85, 86]

$$\Delta \left(\frac{1}{n_{eff}^2} \right)_x = -\frac{2(\Delta n_{eff})_x(x, y, z)}{(n_{eff,0})^3} = P_{11}\varepsilon_x(x, y, z) + P_{12}[\varepsilon_y(x, y, z) + \varepsilon_z(x, y, z)] \quad (3.5)$$

$$\Delta \left(\frac{1}{n_{eff}^2} \right)_y = \frac{2(\Delta n_{eff})_y(x, y, z)}{(n_{eff,0})^3} = P_{11}\varepsilon_y(x, y, z) + P_{12}[\varepsilon_x(x, y, z) + \varepsilon_z(x, y, z)] \quad (3.6)$$

where $n_{eff,0}$ is the effective refractive index of the unstressed FBG and ε_x , ε_y , ε_z are the components of strain at any point (x, y, z) in the X, Y and Z directions respectively.

3.1.2 Polarization and Birefringence

When the FBG is subjected to a lateral load, the pressure distribution at the contact surface is not uniform and therefore the resultant stress distribution is not isotropic in

nature. This develops a refractive index modulation in the perpendicular x and y direction which makes the fiber birefringent. Consequently, two plane-polarized waves will propagate with the principal axes of polarization parallel and perpendicular to the direction of the applied load. A schematic of the propagation of X and Y polarized wave components in a transversely loaded FBG is shown in Figure 3.2. The arrows represent the direction of electric field.

Figure 3.2 Propagation of X and Y polarized wave components in a transversely loaded FBG. The arrows represent the direction of electric field (a) X polarization and (b) Y polarization.

We assume the birefringence induced by the manufacturing and the writing process to be very small compared to the birefringence induced by the applied lateral load. If the intrinsic birefringence of the fiber is very small, the induced birefringence B due to the applied transverse load is given by

$$B = \frac{|\Delta n_y - \Delta n_x|}{n_{co}} \quad (3.7)$$

where n_{co} is the initial core refractive index of the undisturbed optical Bragg grating fiber; n_x and n_y are the refractive indices parallel and perpendicular respectively to the direction of the applied load.

3.1.3 Wavelength shift

The change in the Bragg center wavelength of the FBG due to the applied stress field was given by [84, 85, 86]

$$\begin{aligned}\Delta\lambda_{Bx}(x, y, z) = & -\frac{(n_{eff,0})^3 \Lambda_{B,0}}{E} \times \{ (P_{11} - 2\nu P_{12}) \sigma_x(x, y, z) + [(1 - \nu)P_{12} - \nu P_{11}] \times [\sigma_y(x, y, z) + \sigma_z(x, y, z)] \} \\ & + 2 \frac{n_{eff,0} \Lambda_{B,0}}{E} \{ \sigma_z(x, y, z) - \nu [\sigma_x(x, y, z) + \sigma_y(x, y, z)] \}\end{aligned}\quad (3.8)$$

$$\begin{aligned}\Delta\lambda_{By}(x, y, z) = & -\frac{(n_{eff,0})^3 \Lambda_{B,0}}{E} \times \{ (P_{11} - 2\nu P_{12}) \sigma_y(x, y, z) + [(1 - \nu)P_{12} - \nu P_{11}] \times [\sigma_x(x, y, z) + \sigma_z(x, y, z)] \} \\ & + 2 \frac{n_{eff,0} \Lambda_{B,0}}{E} \{ \sigma_z(x, y, z) - \nu [\sigma_x(x, y, z) + \sigma_y(x, y, z)] \}\end{aligned}\quad (3.9)$$

where $n_{eff,0}$ is the effective refractive index of the unstressed FBG and σ_x , σ_y , σ_z are the components of strain at any point (x, y, z) in the X, Y and Z directions respectively. ν is the Poisson's ratio and $\Lambda_{B,0}$ represents the period of the FBG at zero load. P_{11} and P_{12} are the photoelastic coefficients and E is the Young's modulus of the optical fiber.

3.1.4 Coupled mode analysis

From equations 2.19 and 2.20, the coupled mode equations for X and Y polarizations can be written as,

$$\frac{d(A_j)_x}{dz} = i \sum_k A_{kx} \left((K'_{kj})_x + K^z_{kj} \right) \exp[i(\beta_{kx} - \beta_{jx})z] + i \sum_k B_{kx} \left((K'_{kj})_x - K^z_{kj} \right) \exp[-i(\beta_{kx} + \beta_{jx})z] \quad (3.10)$$

$$\frac{d(B_j)_x}{dz} = -i \sum_k A_{kx} \left((K'_{kj})_x - K^z_{kj} \right) \exp[i(\beta_{kx} + \beta_{jx})z] - i \sum_k B_{kx} \left((K'_{kj})_x + K^z_{kj} \right) \exp[-i(\beta_{kx} - \beta_{jx})z] \quad (3.11)$$

$$\frac{d(A_j)_y}{dz} = i \sum_k A_{ky} \left((K'_{kj})_y + K^z_{kj} \right) \exp[i(\beta_{ky} - \beta_{jy})z] + i \sum_k B_{ky} \left((K'_{kj})_y - K^z_{kj} \right) \exp[-i(\beta_{ky} + \beta_{jy})z] \quad (3.12)$$

$$\frac{d(B_j)_y}{dz} = -i \sum_k A_{ky} \left((K'_{kj})_y - K^z_{kj} \right) \exp[i(\beta_{ky} + \beta_{jy})z] - i \sum_k B_{ky} \left((K'_{kj})_y + K^z_{kj} \right) \exp[-i(\beta_{ky} - \beta_{jy})z] \quad (3.13)$$

The longitudinal coupling coefficient is assumed to be same for both X and Y polarizations. The longitudinal coefficient is generally $K^z_{kj}(z) \ll K'_{kj}(z)$ for fiber modes, and thus this coefficient is usually neglected. And the transverse coupling coefficient between modes j and k for X and Y polarizations can be respectively written as,

$$(K'_{kj})_x(z) = \frac{\omega}{4} \iint_{\infty} dx dy \Delta\eta(x, y, z) \vec{e}_{k,x}(x, y) \cdot \vec{e}_{j,x}(x, y) \quad (3.14)$$

$$(K'_{kj})_y(z) = \frac{\omega}{4} \iint_{\infty} dx dy \Delta\eta(x, y, z) \vec{e}_{k,y}(x, y) \cdot \vec{e}_{j,y}(x, y) \quad (3.15)$$

$\Delta\eta_x$ and $\Delta\eta_y$ are the perturbation to the permittivity due to the applied load.

$$\Delta\eta_x = \eta_0 (n_x - n_{co})^2 \quad (3.16)$$

$$\Delta\eta_y = \eta_0 (n_y - n_{co})^2 \quad (3.17)$$

η_0 and n_{co} respectively represent the permittivity and core refractive index of undisturbed FBG.

$$(K'_{kj})_x(z) = \frac{\omega}{4} \eta_0 \iint_{\infty} (n_x - n_{co})^2 dx dy \vec{e}_{k,x}(x, y) \cdot \vec{e}_{j,x}(x, y) \quad (3.18)$$

$$(K'_{kj})_y(z) = \frac{\omega}{4} \eta_0 \iint_{\infty} (n_y - n_{co})^2 dx dy \vec{e}_{k,y}(x, y) \cdot \vec{e}_{j,y}(x, y) \quad (3.19)$$

The amplitude reflection coefficients ρ and power reflection coefficient $r = |\rho|^2$, respectively,

$$\rho_x = \frac{-k_x \sinh\left(\sqrt{k_x^2 - \hat{\sigma}_x^2} L\right)}{\hat{\sigma}_x \sinh\left(\sqrt{k_x^2 - \hat{\sigma}_x^2} L\right) + i\sqrt{k_x^2 - \hat{\sigma}_x^2} \cosh\left(\sqrt{k_x^2 - \hat{\sigma}_x^2} L\right)} \quad (3.20)$$

$$\rho_y = \frac{-k_y \sinh\left(\sqrt{k_y^2 - \hat{\sigma}_y^2} L\right)}{\hat{\sigma}_y \sinh\left(\sqrt{k_y^2 - \hat{\sigma}_y^2} L\right) + i\sqrt{k_y^2 - \hat{\sigma}_y^2} \cosh\left(\sqrt{k_y^2 - \hat{\sigma}_y^2} L\right)} \quad (3.21)$$

$$r_x = \frac{\sinh^2\left(\sqrt{k_x^2 - \hat{\sigma}_x^2} L\right)}{\cosh^2\left(\sqrt{k_x^2 - \hat{\sigma}_x^2} L\right) - \frac{\hat{\sigma}_x^2}{k_x^2}} \quad (3.22)$$

$$r_y = \frac{\sinh^2\left(\sqrt{k_y^2 - \hat{\sigma}_y^2} L\right)}{\cosh^2\left(\sqrt{k_y^2 - \hat{\sigma}_y^2} L\right) - \frac{\hat{\sigma}_y^2}{k_y^2}} \quad (3.23)$$

The reflectivity functions can be considered as vector quantities. Therefore the reflectivity of the disturbed FBG for unpolarized propagating light can be written as,

$$\vec{r} = \vec{r}_x + \vec{r}_y \quad (3.24)$$

If the magnitude alone is considered, $r^2 = r_x^2 + r_y^2$

The coupling coefficients, $k_x = (K_{kj}^t)_x$ and $k_y = (K_{kj}^t)_y$ (3.25)

$\hat{\sigma}$ is approximately equal to the phase mismatching coefficient.

$$\hat{\sigma}_x \equiv \beta - \beta_{Bx} = 2\pi n_{eff,0} \left(\frac{1}{\lambda} - \frac{1}{\lambda_{Bx}} \right) \quad (3.26)$$

$$\hat{\sigma}_y \equiv \beta - \beta_{By} = 2\pi n_{eff,0} \left(\frac{1}{\lambda} - \frac{1}{\lambda_{By}} \right) \quad (3.27)$$

Equations 3.20-3.27 can be utilized to study the spectral response of the FBG during the lateral compression process. The stress distribution and the refractive index modulation obtained from equations 3.1-3.4 can be substituted in these equations. The coupling coefficients can be calculated using equations 3.18-3.19.

3.2 WAVELENGTH TUNING THROUGH TRANSVERSE LOADING OF FBG: LITERATURE REVIEW

Figure 3.3 A schematic showing the wavelength tuning of FBG by lateral compression in which the fiber containing the grating is compressed diametrically between 2 plates. ϵ_x and ϵ_y represent the strains in the X and Y direction.

Figure 3.3 shows the schematic in which the load acting on the FBG in the lateral or radial (transverse) direction is utilised for tuning the spectral response. The fiber containing the grating is compressed transversely or diametrically using two compressing medium as shown in the figure. A schematic of the resulting strained fiber is also shown in figure 3.3. It is different from the case of axial tension and compression that the principal stress is acting in the radial direction perpendicular to the axis of the fiber. A small component of the induced radial stress will be acting in the

axial direction. However its magnitude is very small and the overall wavelength tuning mechanism will be dominated by the radial strains.

The effect of lateral load on a FBG was first reported in 1996 [87]. The applied lateral stress on the FBG fabricated on a low birefringent single mode silica fiber caused a shift in x-polarization $\Delta\lambda_x$ which is 10 times higher than y-polarization $\Delta\lambda_y$. The difference in the stress distribution in the X and Y direction establishes different response criteria for the X and Y polarizations. Also due to low Poisson's ratio of silica (0.16), only a small component of the resultant stress will be acting in the directions perpendicular to the direction of the applied force. The wavelength shift sensitivity of x-polarization $\Delta\lambda_x$ was only 0.00326 nm/N for a loading range of 50 N (i.e., 30-80 N). Hence the total wavelength shift in $\Delta\lambda_x$ is less than 0.2 nm for an applied lateral load of 80 N.

In another related studies also [88, 89], the polarization state of the incident light and the compression condition was found to have significant effect on the spectral response of FBG under lateral compression. The difference between the reflection responses for X-and Y-polarizations was found to be increasing linearly with increase in the lateral compression load.

The transverse load response of FBGs fabricated in high birefringent (Hibi) optical fibers has also been reported [90]. In the case of diametrical compression of PM fibers, where an initial peak separation exists due to the intrinsic birefringence of the fiber, the applied transverse load changes the existing initial peak separation. Here, the peak shift also depends on the loading angle [91]. The response to transverse load applied at an angle to the fast and slow axes is also non-linear if the strains due to loading are of the order of the residual strains generating the birefringence [91]. The

stress concentration produced by the applied lateral load responsible for polarimetric phase sensitivity that is a function of the applied load direction. Also, the transverse applied load does not significantly alter the principal directions caused by the residual stresses generated by the manufacturing conditions [90].

A comparison of transverse load sensitivities of fiber Bragg gratings (FBGs) fabricated in a range of commercially available stress and geometrically induced high birefringent (HiBi) fibers have also been reported [92]. The highest transverse load sensitivity, of 0.23 ± 0.02 nm/N/mm, was reported with HiBi FBGs fabricated in elliptically clad fiber. This was nearly 25% higher than for any other HiBi fiber, which, coupled with the small diameter of the fiber, makes it a good candidate for an embedded or surface mounted strain sensor [92].

In all the above cases, the loading configuration under consideration was diametrical compression test. Bosia et al. [91] considered a case which is more close to real application, namely, with the fiber embedded in a specimen. This is because the fiber has to be mounted on some specimen or substrate for all practical applications. Similar to the diametrical compression tests, in the case of FBG in SM fiber, the loading induces a split of the reflected spectrum into two separate peaks. In contrast to the diametrical compression case, strong tensile axial strains were also present and therefore considerable overall wavelength shift was present in addition to the splitting of the reflected peak. For FBG written in PM fibers, the initial peak separation also allows the discrimination of the wavelength shifts of both peaks even for small applied loads.

A localised lateral pressure, induced by pressing a small region as shown in Figure 3.4 induces a phase shift and shapes the spectral response of the FBG [93]. The shape of the reflected spectrum depends on the pressing location. If the index

modulation amplitude is symmetrical, and the grating is pressed at the center, the sub gratings are identical which completely cancels the reflection exactly at grating center wavelength. The applied strain determines the wavelength for which complete cancellation occurs.

Figure 3.4 Schematic showing the set up for wavelength shaping by localized lateral pressure [93]

It was shown that the narrow transmission peak of a π -phase-shifted FBG could also be utilized for transverse load sensing [94]. Higher resolution is possible owing to the smaller width of the spectral peak as shown in Figure 3.5. However, such a scheme requires very high subpicometer resolution wavelength detection technique.

Figure 3.5 Transmission spectrum of a π -phase shifted Bragg grating [94]

Thus to summarize, the transverse load applied to the FBG fabricated in normal fiber will change its birefringence and cause reflection spectrum splitting. And in the case of a PM fiber, the transverse load changes the spectral separation of the two polarization modes.

3.3 TRANSVERSE LOAD SENSING WITH ATTACHED MECHANICAL STRUCTURES AND USING SPECIAL GRATINGS

Figure 3.6 shows the schematic of a lateral pressure which could be used to measure fluid pressure [95]. Here the FBG is encapsulated in a polymer filled metallic cylinder with a pressure-sensing hole. The device has very high pressure sensitivity, but can only be used for measuring fluid pressure and is not suited for other applications.

Figure 3.6 Lateral pressure sensor for measuring fluid pressure [95]

Another design of a transversally loaded pressure transducer with mechanically enhanced sensitivity is shown in Figure 3.7 [96]. The transducer was designed for measuring gas pressures and consists of a diaphragm attached to a piston and open to the pressure line. The system comprises of a complicated mechanical structure, but the design does not address the inherent limitations of wavelength tuning through transverse loading.

Figure 3.7 Design of a transversally loaded pressure transducer with mechanically enhanced sensitivity [96]

Liu *et al.* proposed [97] the use of long-period fiber gratings (LPGs) for transverse strain measurement. It was demonstrated that (LPGs) are highly sensitive to transverse loading, the sensitivity being several 100 times higher than normal FBGs. However LPGs are a different class of gratings and suffers from poor thermal stability. LPGs need very lengthy gratings, which is very much inconvenient for structural sensing applications. Moreover, LPGs are highly sensitive to bending. Again, the technique was based on the polarization mode splitting effect, thus requiring measurements for the two orthogonal polarization modes. This not only adds to the complexity and cost of the detection system but also makes it non-applicable for dynamic load measurement.

Another proposed scheme [98] was based on the use of a reversible sampled fiber Bragg grating (SFBG) produced by a mechanical device as shown in Figure 3.8. A photoinduced uniform period FBG is sandwiched between two metal plates. The bottom

plate has two V grooves to accommodate a FBG and a balancing fiber, respectively. The correlation between the reflectivities of the Fourier components of the sampled structure and the applied transverse strain provides the mechanism to measure the transverse load. The load is measured by the reflectivity of the first-order Fourier component of the induced SFBGs. The prominent disadvantage of the proposed method is that the system is based on the reflected power measurement and not on the wavelength shift like conventional FBG sensors. Moreover, the presence of the mechanical structure makes the sensor more complicated and therefore inconvenient for embedding or surface mounting.

Figure 3.8 Lateral pressure tuning of the mechanically induced sampled grating [98]

3.4 LPT FOR SENSING APPLICATIONS

Despite the most evident shortcomings of wavelength tuning by transverse pressure, it is still used for quite a few applications [99-100]. Most of them are embedded FBGs in which the hydrostatic pressure acting in the radial direction is utilised for the wavelength tuning process.

a) Health monitoring and damage detection of concrete structures

FBGs embedded in or surface mounted onto concrete could be used to measure strain and temperature during curing and prestress conditions. The strain induced by the plastic shrinkage of concrete during the hardening process, modulated the spectral response of the FBG. FBG sensors can be used to qualitatively monitor the structural integrity, from the time of concrete casting, through the material's service life [99]. Embedded optical fiber Bragg grating sensors are also being used to evaluate the properties of high performance concrete subjected to high thermal and mechanical strain [100]. FBGs embedded in cement paste specimens have also been utilized for investigating the early age shrinkage and to determine the shrinkage strains starting from the beginning of the cement setting process [101]. Moreover, embedded FBG sensors could be used to predict, detect and locate structural damages and cracks when the structure is subjected to heavy loading [99].

b) Cure monitoring and damage detection of composites

FBG sensors embedded in smart composite materials could be used to monitor the curing process [102] and to detect the structural damages [103, 104]. The pressure and temperature changes during the curing process modulate the spectral response of the FBG sensor. FBG sensors could be used for the impact damage detection in carbon fiber reinforced plastic (CFRP) [105].

It has also been shown that embedded optical fibre Bragg grating sensors have considerable potential for the structural health monitoring of adhesively bonded composite ship joints. Bragg grating sensors embedded at various locations along the bond-line of adhesively bonded composite joints response to interfacial disbonds [106].

It has also been demonstrated that internal strain measurements using embedded FBG sensors, accompanied by appropriate modeling, can be used to characterize deformation and fracture behavior of polymeric materials at various scales [107]

c) Other applications

Transverse wavelength has also been utilized to characterize the stress field on a bare optical fiber inside a connector [108]. The connector exerts lateral compressive stresses on the fiber, which yields a shift in the Bragg center wavelength.

Use of transversally loaded FBGs sensors has also been reported for aerospace applications [109]. Such FBG monitoring systems can greatly improve the health management systems for aerospace vehicles.

3.5 ADVANTAGES OF WAVELENGTH TUNING THROUGH LATERAL LOADING FOR SENSING APPLICATIONS

Compared to axial loading, the wavelength tuning process through transverse loading offers some unique advantages for sensing applications. However, these advantages still remain unexplored due to the inherent limitations of the lateral pressure tuning process. The wavelength tuning of the FBG through lateral loading has the advantage of easy surface mounting, robustness, long term stability, structural integrity and more resistance to vibration. Owing to the same reason, lateral tuning is the ideal option for vibration and dynamic strain measurements, especially in harsh environments. For the long-term monitoring purposes, the structural integrity, reliability, durability and accuracy of a sensory system are critical. Wavelength tuning through lateral or transverse loading is highly attractive for applications like structural health monitoring and damage detection owing to the flexibility in mounting or embedding the sensor.

However, the full potentials of wavelength tuning through lateral loading are yet to be utilized overcoming the existing limitations.

3.6 ISSUES TO BE ADDRESSED IN WAVELENGTH TUNING THROUGH LATERAL LOADING

These problems have to be addressed in order to improve the performance of existing transversely loaded FBGs sensors and to increase the application range of FBG sensors.

a) Low lateral pressure sensitivity

The sensitivity of peak shifting to the applied lateral load is extremely low which affects its suitability for load sensing applications. Even though the splitting or the separation of the reflection peak can also be utilized, its magnitude is generally too small compared to the bandwidth of the reflected peak. The reported value of lateral pressure sensitivity of bare FBG is less than 0.0035 nm/N [87].

b) Narrow tuning range

The maximum achievable wavelength shift by diametrical compression is less than 0.4 nm, which is too small for most applications. Owing to the low sensitivity, very large loads are required to achieve this tuning range which consecutively affects the reliability of the device.

c) Birefringence: Peak splitting and pulse broadening

The birefringence developed due to non-uniform distribution of stresses across the cross section of the FBG affects the quality of the sensing signal. The X and Y polarizations responds differently to the applied load depending on the stress gradient. A polarization detection scheme, which measures the two orthogonal polarization

modes, is necessary to interrogate the response signals. This increases the complexity and cost of the detection system.

d) Influence of contact conditions

The contact conditions between the fiber and the contact medium in the lateral compression process are very important to obtain the expected spectral response. The fiber diameter of 125 μm implies that the contact area is small, which means that a highly smooth and flat compressing plate is necessary. Moreover, the contact friction between the contact surfaces is very important. However, no efforts have been reported to characterize the influence of the contact conditions and to optimise it for enhancing the performance of lateral tuning process. The theoretical formulations for stress distribution and refractive index modulation (presented in Section 3.1) does not account for the contact medium material, its configuration and contact conditions and the boundary conditions.

e) Reliability issues

Silica fiber is fragile and highly prone to fracture. The wavelength tuning by transverse loading raises a serious question on the ability of the fiber to resist the static and cyclic mechanical stresses. The cyclic nature of the stresses for repeated tuning also brings in the need for fatigue life evaluation. Even though the reliability of FBGs subjected to axial loading has extensively been studied by various researchers [118-125], no work has been done addressing the reliability upon transverse loading. It is necessary to ensure that the required performance of the laterally loaded FBG sensors is achievable at stress levels and frequencies well below the critical limit for immature failure.

Chapter 4

OPTIMIZATION OF THE CONTACT MEDIUM

4.1 INTRODUCTION

The wavelength response characteristic of an FBG subjected to a lateral load is highly dependent on the pressurizing (or contact) medium. However no related work has been reported addressing this issue. Therefore, it is essential to characterize the influence of the pressurizing medium and to optimize its configuration to enhance the wavelength tuning process. In this chapter, the effect of pressurizing medium, its configuration and the contact conditions is investigated theoretically and experimentally. Analysis is carried out to evaluate the influence of the contact material properties on the contact length and the stress distribution on the FBG followed by the analysis of its influence on the wavelength response characteristics. The effect of the thickness of the contact medium and the contact friction between the FBG and the contact medium is also evaluated. Theoretical formulation is followed by finite element analysis and experimental studies.

4.2 ANALYSIS OF THE PRESSURE TRANSMISSION SYSTEM

Consider the typical transverse loading configuration as shown in Figure 4.1. The fiber containing the grating is compressed between two plates. The loading axis is designated as Y and axis parallel to the central longitudinal axis of the fiber is designated as Z. The

applied load P is assumed to be acting uniformly over the contact plate. Let t be the thickness of the contact plate, b the breadth and L the grating length under consideration. The deformation mode and the magnitude of stresses and strains depend upon the geometry of the contact plate, the boundary conditions and material properties. These aspects are analyzed in detail in the following sections.

Figure 4.1 Schematic of the lateral loading configuration of the FBG and co ordinate system

4.2.1 Contact length

When the fiber containing the grating is compressed laterally, as shown in Figure 4.1, the contact between the fiber and plate is a line at zero loads and low loads (line contact).

Figure 4.2. Schematic representing the contact between the fiber and the pressurizing medium.

As the load increases the contact area also increases. The contact width $2a$ can be expressed as [112, 113]

$$2a = 4 \left[\frac{PR}{\pi L} \frac{(1 - \nu_2^2)E_1 + (1 - \nu_1^2)E_2}{E_1 E_2} \right]^{1/2} \quad (4.1)$$

where P is the applied load over a length l , R is the radius of the fiber, E_1 and E_2 represents the Young's modulus of fiber and the pressurizing medium, respectively, and ν_1 and ν_2 represent the respective Poisson's ratio. The suffix 1 and 2 are used respectively to describe the properties of the fiber and the contact medium.

From this equation, it could be seen that the contact width is dependent on the pressurizing medium material. The variation in contact length between the fiber and the compressing plate, with respect to the applied load for different Young's modulus values of a metallic and polymeric compressing plate, is shown graphically in Figures 4.3 and 4.4. The length of the FBG under consideration is 10 mm. The Young's modulus and Poisson's ratio of the single mode silica fiber with diameter 125 μm are taken respectively as 72.5 GPa and 0.16.

Figure 4.3. Graph showing the effect of stiffness E of a metallic contact medium on the contact length at various loads P

Figure 4.3 shows the effect of the stiffness, which typically ranges from 1 GPa to 300 GPa, of a metallic contact plate. The typical value of Poisson’s ratio of metal assumed for the analysis is 0.30. It could be seen that the contact length for a particular load increases exponentially with the decrease in the stiffness of the compressing plate. For example, for a load of 1 N, the contact length is less than 2 micrometer for $E > 10$ GPa. This can be considered as a line contact where the contact area is close to zero. If E is reduced below 10 GPa, the contact length increases drastically as can be seen from the graph, for example, to 5.5 micrometer for $E=1$ GPa.

Figure 4.4 shows the effect of the stiffness, which typically ranges from 10 MPa to 500 MPa, of a polymeric contact plate. The typical value of Poisson’s ratio of the polymer assumed for the analysis is 0.45. Comparison of Figures 4.3 and 4.4 shows that the contact length of a polymeric compressing plate is extremely high compared to metallic plate. For example, for a load of 1 N, the contact length which is less than 7

micrometer for $E=500$ MPa increases dramatically to as high as 50 micrometer for $E=10$ MPa. It can be inferred that, for the same load, polymeric plates can give contact length much higher than that of metallic compressing plates.

Figure 4.4. Graph showing the effect of stiffness E of the polymeric contact medium on contact length at various loads P

The practical implication of higher contact length is that the load will be distributed over a larger area along the circumference of the FBG. Therefore the nature of the applied load becomes more hydrostatic which leads to the reduction in the difference between the stresses in the perpendicular directions.

4.2.2 Stress distribution upon lateral compression

The nature and magnitude of stresses in the fiber during the lateral compression process depends on the contact medium material and the loading configuration. Various cases are discussed in the following sections. Let σ_{x1} , σ_{y1} , σ_{z1} and σ_{x2} , σ_{y2} , σ_{z2} be the stress

components in the X,Y,Z directions in the fiber and the contact medium, respectively, and ε_{x1} , ε_{y1} , ε_{z1} and ε_{x2} , ε_{y2} , ε_{z2} are the corresponding strains.

4.2.2.1 Case 1 Incompressible (rigid) contact medium

An incompressible contact medium is characterized by a high value of Young's modulus and the contact length $2a$ is very small as seen from the analysis in section 4.2.1. Therefore the influence of the contact area can be neglected and the analysis can be considered as the static problem of pure diametrical compression. Plane strain conditions are prevailing owing to the large length of the fiber (several mms) in comparison to the diameter (125 μm) which means that the axial strain $\varepsilon_z \rightarrow 0$. Also, if the contact medium is rigid, the deformation of the contact plate is negligible and therefore the boundary conditions applied on the contact plate do not have any significant effect on the deformation mode. The load applied on the contact plate transfers completely to the fiber and there exists no physical contact between the upper and lower plates during the loading process.

Figure 4.5. Schematic of FBG subjected to lateral pressure tuning using an incompressible contact medium.

Thus the stress distribution at any point (r, θ) is independent of the contact conditions and can be expressed as, (The detailed derivation of these equations is given in Appendix A)

$$\sigma_x(r, \theta) = \frac{-2P}{\pi L} \left\{ \frac{r^2 \cos^2 \theta (R + r \sin \theta)}{[r^2 + R^2 + 2Rr \sin \theta]^2} + \frac{r^2 \cos^2 \theta (R - r \sin \theta)}{[r^2 + R^2 - 2Rr \sin \theta]^2} - \frac{1}{2R} \right\} \quad (4.2)$$

$$\sigma_y(r, \theta) = \frac{-2P}{\pi L} \left\{ \frac{(R + r \sin \theta)^3}{[r^2 + R^2 + 2Rr \sin \theta]^2} + \frac{(R - r \sin \theta)^3}{[r^2 + R^2 - 2Rr \sin \theta]^2} - \frac{1}{2R} \right\} \quad (4.3)$$

Analyzing equations 4.1 to 4.3, it can be seen that the stress distribution at any point (r, θ) is directly proportional to the applied load and inversely proportional to the length L of the FBG. Since the contact medium is incompressible, there is no change in the contact conditions and it is independent on the magnitude of the applied load. As can be seen from Fig. 4.3, the incompressible contact medium characterized by a typically large value of Young's modulus is associated with very small values of

contact length and therefore there is no problem in assuming a line contact between the contact medium and the fiber. The contact length (though $\rightarrow 0$) remains invariant with the applied load. As the contact length (and hence contact area) tends to zero, the issue of contact friction does not come into picture as evident from the absence of frictional factor in Eqns. 4.2 and 4.3.

4.2.2.2 Case 2: Unconstrained compressible contact medium

If the contact medium is deformable, it wraps around the fiber during the loading process, the contact area being dependent on the Young's modulus values as depicted in the analysis in section 4.2.1. Unlike the previous case, the deformation of the contact plate is not negligible and therefore deformation of the fiber is not solely due to the load transferred in the radial direction through the contact plate. Here, the axial (Z-direction) deformation of the contact plate highly influences the effective strain in the fiber.

Since the gap between the plates is very small compared to the deformation (strain) in Y direction of the contact medium, both the plates come into contact (except those portion adjacent to the fiber) during the loading process.

Since the physical dimensions of the contact plate is very large compared to that of the optical fiber, the stress concentration at the contact area can be neglected for calculating the effective strain in the contact plate. Consider Figure 4.6 in which ABCD represents the end face (XY plane) of the contact plate and A'B'C'D' represents the deformed shape.

Figure 4.6. Schematic of FBG subjected to lateral pressure tuning using an unconstrained compressible contact medium. The dotted lines represent the deformed state after the application of the load.

If P is the load acting uniformly over the area BCGF, then the stress in the contact medium in the Y direction σ_{y2} can be written as

$$\sigma_{y2} = \frac{P}{bL} \quad (4.4)$$

$$\sigma_{z2} = -\nu_2 \sigma_{y2} \quad (4.5)$$

Therefore the axial strain in the contact plate is

$$\varepsilon_{z2} = -\frac{\nu_2 \sigma_{y2}}{E_2} \quad (4.6)$$

where ν_2 is the Poison's ration of the contact medium. Therefore the axial strain coupled to the fiber due to axial deformation of the contact plate can be written as $k \cdot \mu_{12} \cdot \varepsilon_{z2}$, where μ is the coefficient of sliding friction at the contact surface and k is factor representing the comparative cross sectional areas in the XY plane and the stiffness of the fiber and the contact plate. Its value can be taken as one if the cross sectional area of the contact plate ($b \times l$) is very large compared to that of the fiber.

$$k = \frac{E_2 A_2 - E_1 A_1}{E_1 A_1 + E_2 A_2} \quad (4.7)$$

Now the intrinsic stresses developed in the fiber due the perpendicular (radial) stresses acting on the fiber at the contact surface has to be derived.

Consider the schematic shown in Figure 4.7. A lateral load P is applied on the contact plate and $2a$ is the corresponding contact length. Let the contact medium subtends an angle of 2ϕ at the center of the fiber and dP be the load acting on an elemental strip at an angle θ_l from the Y-axis. $d\theta_l$ is the width of the elemental strip.

Figure 4.7. Schematic of the contact geometry with a deformable contact medium

The stress distribution at any point (r, θ) in the fiber, whose detailed derivation is shown in Appendix A, can be expressed as,

$$\sigma_x(r, \theta) = \int_{-\phi}^{+\phi} \frac{2 \left(\frac{2P}{\pi} \cos \theta_1 d\theta_1 \right)}{\pi L} \left\{ \frac{r^2 \cos^2(\theta - \theta_1)(R + r \sin(\theta - \theta_1))}{[r^2 + R^2 + 2Rr \sin(\theta - \theta_1)]^2} + \frac{r^2 \cos^2(\theta - \theta_1)(R - r \sin(\theta - \theta_1))}{[r^2 + R^2 - 2Rr \sin(\theta - \theta_1)]^2} - \frac{1}{2R} \right\} \quad (4.8)$$

$$\sigma_y(r, \theta) = \int_{-\phi}^{+\phi} \frac{2 \left(\frac{2P}{\pi} \cos \theta_1 d\theta_1 \right)}{\pi L} \left\{ \frac{(R + r \sin(\theta - \theta_1))^3}{[r^2 + R^2 + 2Rr \sin(\theta - \theta_1)]^2} + \frac{(R - r \sin(\theta - \theta_1))^3}{[r^2 + R^2 - 2Rr \sin(\theta - \theta_1)]^2} - \frac{1}{2R} \right\} \quad (4.9)$$

where $\phi = \frac{a}{R}$ and $2a$ is the contact length.

$$\therefore \varepsilon_{z1} = \frac{\sigma_z}{E} = -\frac{\nu}{E} [\sigma_x(r, \theta) + \sigma_y(r, \theta)] \quad (4.10)$$

Therefore the net axial strain in the fiber ε_{zf} can be expressed as sum of the strain component developed due to the radial stresses and the strain which is coupled to the fiber due to the axial deformation of the contact medium.

$$\varepsilon_{zf} = \varepsilon_{z1} + k \cdot \mu_{12} \cdot \varepsilon_{z2} \quad (4.11)$$

In this case, it could be seen that the stresses in the fiber is independent of the thickness t while it depends on other dimensions (b and L) and the material properties. In addition to this, the contact friction plays an important role as seen from the equations. It is evident from Eqn. 4.11 that a larger contact friction is recommended for a strain in the Z direction and therefore for enhanced wavelength shift sensitivities.

However, the above discussed case is very ideal in which the contact medium is fully unconstrained. An example is the case in which the contact medium is compressed by fluid pressure-air or liquid.

4.2.2.3 Case 3: Compressible contact medium with bottom and top surface constrained

Here the top and bottom surfaces are fixed, so as to constrain its motion in the XZ plane, allowing it to move only in Y direction, as shown in Figure 4.8. This is equivalent to the pressurizing medium being attached to another medium. Most of the practical cases are similar to this. ABCDEFGH is the undeformed contact medium and A'B'C'D'E'F'G'H' represents the deformed shape upon application of the lateral load over the face BCGF.

Figure 4.8. FBG subjected to lateral pressure tuning using a compressible contact medium with top and bottom surfaces constrained. The dotted lines represent the deformed state after the application of the load.

Similar to the previous case, the stress in the contact plate in the Y direction σ_{y2} can be written as

$$\sigma_{y2} = \frac{P}{bL} \quad (4.12)$$

Therefore,

$$\sigma_{z2} = -\nu_2 \sigma_{y2} \quad (4.13)$$

But unlike the previous case, the axial strain in the contact plate ε_{z2} is not uniform along Y-axis. In order to find the strain distribution along Y-axis, consider an elemental strip of thickness dt at a distance t_1 from the fixed end, as shown in Figure 4.9

Figure 4.9. Schematic showing the axial deformation of the contact plate

The total force acting on the strip in the Z direction $= \sigma_{z2} \cdot b \cdot dt_1$

Taking the moment,

$$EI \frac{d^2 \delta}{dt_1^2} = \sigma_{z2} \cdot \frac{bt_1^2}{2} \cdot dt_1 \quad (4.14)$$

where I is the moment of inertia and δ , the deflection.

$$EI \frac{d\delta}{dt_1} = \sigma_{z2} \cdot \frac{bt_1^3}{6} + C_1 \quad (4.15)$$

$$EI\delta(t_1) = \sigma_{z2} \cdot \frac{bt_1^4}{24} + C_1 t_1 + C_2 \quad (4.16)$$

Applying the boundary conditions,

At $t_1=0$,

$$\delta=0 \quad (4.17)$$

Also, at $t_1=0$,

$$\frac{d\delta}{dt_1} = 0 \quad (4.18)$$

Therefore both the constants C_1 and C_2 are equal to zero.

$$C_1=0, C_2=0 \quad (4.19)$$

At $t_1=t$, the deflection δ can be written as,

$$\delta(t) = \frac{\sigma_{z2}}{EI} \cdot \frac{bt^4}{24} \quad (4.20)$$

Therefore the strain in the Z-direction at $t_1 = t$ is

$$\varepsilon_z(t) = \frac{2\delta(t)}{L} \quad (4.21)$$

Similar to case 1, the axial strain coupled to the fiber can be thus expressed as,

$$k \cdot \mu_{12} \cdot \varepsilon_z(t)$$

Equations 4.8 and 4.9 represent the intrinsic stresses developed in the fiber due to the radial stress acting on the fiber at the contact area. The net axial strain produced in the fiber can be expressed as,

$$\varepsilon_{zf} = \varepsilon_{z1} + k \cdot \mu_{12} \cdot \varepsilon_z(t) \quad (4.22)$$

Thus, in this case, the axial elongation of the fiber depends also on the thickness in addition to the coefficient of friction of the contact plate. From the analysis, it could be seen that a higher value of t is favorable for bigger axial elongation. However t cannot be made too high due to engineering limitations in practical applications. However a better compromise between contact friction and t values can be achieved to enhance the wavelength shift sensitivity.

4.2.3 Calculation of coupling coefficient and reflectivity

The change in the refractive index due to the presence of the lateral load can be expressed as [84],

$$\Delta n_x(x, y, z) = -\frac{n_0^3}{2E_1} \left\{ (P_{11} - 2\nu P_{12}) \sigma_x(x, y, z) + [(1 - \nu)P_{12} - \nu P_{11}] [\sigma_y(x, y, z) + \sigma_z(x, y, z)] \right\} \quad (4.23)$$

$$\Delta n_y(x, y, z) = -\frac{n_0^3}{2E_1} \left\{ (P_{11} - 2\nu P_{12}) \sigma_y(x, y, z) + [(1 - \nu)P_{12} - \nu P_{11}] [\sigma_x(x, y, z) + \sigma_z(x, y, z)] \right\} \quad (4.24)$$

σ_x , σ_y , and σ_z can be substituted in this equation from section 4.2.2 for each case, depending on the boundary conditions, to obtain the refractive index gradient. Therefore, the transverse coupling coefficient for X and Y polarizations can be calculated as,

$$(K'_{kj})_x(z) = \frac{\omega}{4} \iint_{\infty} dx dy \Delta \eta(x, y, z) \vec{e}_{k,t}(x, y) \cdot \vec{e}_{j,t}(x, y) \quad (4.25)$$

$$(K'_{kj})_y(z) = \frac{\omega}{4} \iint_{\infty} dx dy \Delta \eta(x, y, z) \vec{e}_{k,t}(x, y) \cdot \vec{e}_{j,t}(x, y) \quad (4.26)$$

where $\Delta \eta_x$ and $\Delta \eta_y$ are the change in the permittivity in the X and Y directions due to applied transverse load given by [84],

$$\Delta \eta_x = \eta_0 (n_x - n_0)^2 \quad (4.27)$$

$$\Delta \eta_y = \eta_0 (n_y - n_0)^2 \quad (4.28)$$

where η_0 is the permittivity of free space, n_{co} is the core refractive index at zero load and n_x and n_y represents the refractive indices in the X and Y directions in the presence of a load.

Once the stress distribution, the associated refractive index change in the fiber and the coupling coefficients are obtained, it is possible to theoretically calculate the reflective spectrum due to the applied lateral load using equations 3.20 to 3.27.

4.2.4 Summary of physical analysis of the equations

The following findings can be summarized based on the physical analysis of the equations being developed in section 4.2.

- The contact length and hence the contact area can be controlled by selecting the E and ν values of the fiber and the contact material as inferred from Eqn. 4.1.
- For an incompressible contact media, the stress distribution at any point for a specific load is directly proportional to the applied load and independent of all the material parameters. Since the contact medium is incompressible, there is no change in the contact conditions and it is independent on the magnitude of the applied load.
- For an unconstrained compressible contact medium, the stresses in the fiber are independent of the thickness t while it depends on other dimensions and the material properties. The contact friction plays an important role as seen from the equations. Also, it is evident from Eqn. 4.11 that a larger contact friction is recommended for a strain in the Z-direction and therefore for enhanced wavelength shift sensitivities.
- For a compressible constrained contact medium, the axial elongation of the fiber depends also on the thickness in addition to the coefficient of friction of the contact plate. A higher value of t is favorable for bigger axial elongation.

Though t cannot be made too high due to engineering limitations in practical applications, a better compromise between contact friction and t values can be achieved to enhance the wavelength shift sensitivity.

4.3 FINITE ELEMENT CHARACTERIZATION OF THE DESIGN

4.3.1 Methodology

Finite element simulation of the lateral compression process was carried out to scrutinize the stress distribution on the FBG and to eventually compute the wavelength response characteristics. The simulation was carried out using commercial software ANSYS. The schematic of the model and the co-ordinate system used is shown in Figure 4.10. The pressurizing medium consists of two elements, the contact plate and the rigid plate. The thickness ' t ' and the material properties of the contact plate are varied for repetitive simulation whereas the properties and the configuration of the rigid plate are kept constant. The lateral load is applied perpendicularly on the rigid plate. Three-dimensional models of the fiber and the compressing plate were created using the element types PLANE82, SOLID186 and SOLID45. Contact elements were used to simulate the contact condition between the fiber and the compressing plate. The contact area of the fiber was meshed using the element type CONTA174 and that of the plate using the element type TARGE170. Flexible-flexible contact analysis has been done with the plate designated as the target element and the fiber as the contact element. Finely refined meshing was done for the fiber and the near contact areas while coarse meshing was done for the rigid plate.

Figure 4.10. Schematic of the FE model and the co-ordinate system (figure not to scale). t is the thickness of the contact plate.

The central axis of the fiber is designated as Z and the loading direction is along Y -axis. The applied boundary conditions are similar to those mentioned in section 4.2.2.3. The degree of freedom is arrested in the Y direction ($U_y=0$) at the bottom surface of the bottom rigid plate. The displacement of all the nodes in the X - Y plane at $z = L/2$ is arrested in the Z -direction ($U_z=0$ at $z = L/2$). Again the displacement of all the nodes in the Y - Z plane at $x = 0$ is arrested in the X -direction ($U_x=0$ at $x = 0$). For the fiber, both ends are kept unconstrained while the degree of freedom is arrested in the Z direction at the center of the fiber ($U_z=0$ at $z = L/2$). Plane stress conditions ($\sigma_z=0$) have been assumed since both the ends of the fiber is unconstrained and there is significant strain in the Z -direction.

The single mode silica fiber is considered as an isotropic medium with insignificant intrinsic birefringence. Same photoelastic and mechanical properties but different refractive indices are assumed for the core and the cladding. The refractive index gradient at any point $P(x, y, z)$ in the grating is related to the strain distribution by the equation [84]

$$\Delta \left(\frac{1}{n_{eff}^2} \right)_x = - \frac{2(\Delta n_{eff})_x(x, y, z)}{(n_{eff,0})^3} = P_{11}\varepsilon_x(x, y, z) + P_{12}[\varepsilon_y(x, y, z) + \varepsilon_z(x, y, z)] \quad (4.29)$$

$$\Delta \left(\frac{1}{n_{eff}^2} \right)_y = \frac{2(\Delta n_{eff})_y(x, y, z)}{(n_{eff,0})^3} = P_{11}\varepsilon_y(x, y, z) + P_{12}[\varepsilon_x(x, y, z) + \varepsilon_z(x, y, z)] \quad (4.30)$$

where $n_{eff,0}$ is the effective refractive index of the unstressed FBG and ε_x , ε_y , ε_z are the components of strain at any point $P(x, y, z)$ in the X, Y and Z directions respectively. P_{11} and P_{12} are the photoelastic coefficients and E is the Young's modulus of the optical fiber. The change in the Bragg center wavelength of the FBG, due to the applied stress field, for X and Y polarizations $\Delta\lambda_{Bx}$ and $\Delta\lambda_{By}$ is respectively given by [84]

$$\begin{aligned} \Delta\lambda_{Bx}(x, y, z) = & - \frac{(n_{eff,0})^3 \Lambda_{B,0}}{E} \times \left\{ (P_{11} - 2\nu P_{12})\sigma_x(x, y, z) + [(1 - \nu)P_{12} - \nu P_{11}] \times [\sigma_y(x, y, z) + \sigma_z(x, y, z)] \right\} \\ & + 2 \frac{n_{eff,0} \Lambda_{B,0}}{E} \left\{ \sigma_z(x, y, z) - \nu [\sigma_x(x, y, z) + \sigma_y(x, y, z)] \right\} \end{aligned} \quad (4.31)$$

$$\begin{aligned} \Delta\lambda_{By}(x, y, z) = & - \frac{(n_{eff,0})^3 \Lambda_{B,0}}{E} \times \left\{ (P_{11} - 2\nu P_{12})\sigma_y(x, y, z) + [(1 - \nu)P_{12} - \nu P_{11}] \times [\sigma_x(x, y, z) + \sigma_z(x, y, z)] \right\} \\ & + 2 \frac{n_{eff,0} \Lambda_{B,0}}{E} \left\{ \sigma_z(x, y, z) - \nu [\sigma_x(x, y, z) + \sigma_y(x, y, z)] \right\} \end{aligned} \quad (4.32)$$

σ_x , σ_y , σ_z are the components of stress at any point $P(x, y, z)$ in the X, Y and Z directions respectively. ν is the Poisson's ratio and $\Lambda_{B,0}$ represents the period of the FBG at zero load. The following material properties are used for the analysis. For the

fiber: Young's modulus $E=72.5 \times 10^9 \text{ N/m}^2$ and Poisson's ratio $\nu=0.16$. Effective refractive index of the single mode silica fiber is taken as 1.4682. Photoelastic coefficients, $P_{11}=0.113$ and $P_{12}=0.252$, are used for the analysis. The cladding diameter of the bare FBG is taken as 125 micrometer and the core diameter as 8.2 micrometer. The length ' L ' of the FBG under consideration is 10 mm. The wavelength shift due to the applied lateral load is calculated with respect to a center wavelength of 1550 nm.

Figure 4.11. A close-up view of the meshed FE model (a) End view (b) Isometric view

Since the issue of interest is the influence of contact plate on the lateral pressure tuning, the variables in the simulation are the thickness of the contact plate, its stiffness and the contact friction. Both the length and width of the contact plate is fixed as 10 mm. Repetitive simulation was carried out by varying each of these parameters and evaluating its wavelength response. A representative model of the finite element mesh is shown in Figure 4.11. Upon application of a test load, it was found that the contact area is increasing in each load step while the deformation progresses. The fiber is compressed in the radial direction along with a positive longitudinal strain in the axial

direction whose magnitudes were highly dependent on the stiffness of the contact medium, its thickness and the contact friction.

The following analysis is done for an applied load of 5 N acting perpendicularly over the top plate under various conditions discussed below. Here the detailed analysis is confined only to polymeric contact medium because of its superior performance compared to metallic plates as inferred from the analysis in section 4.2.1

4.3.2 Axial and radial strains

Figure 4.12 shows the axial and radial strain at the center of the FBG at different thickness, t , of the contact plate with stiffness $E=500$ MPa and contact friction $\mu=0.3$. It could be seen that ε_y , the strain in the loading Y-direction is compressive in nature whereas the strain in the perpendicular radial direction ε_x is tensile. Since the nature of the applied load in the Y-direction is compressive, it is quite obvious that the corresponding strain ε_y remains compressive all the time irrespective of the magnitude of the applied load or the thickness t . The magnitude of compressive strain ε_y increases until a thickness of 8 mm and remains almost constant with further increase in thickness. The radial strain in the x direction ε_x decreases with increase in thickness and remains tensile for thicknesses up to 10 mm. However the rate of drop off reduces for thicknesses above 5 mm. ε_z , the strain in the axial direction, increases till $t=3$ mm and remains almost constant for further increase in thickness.

Figure 4.12. Graph showing the dependence of axial and radial strains on t at center of the FBG for $E=500$ MPa of the pressurizing medium

Figure 4.13 represents the analyses of the case with reduced stiffness, $E=50$ MPa, of the contact plate. ϵ_x , ϵ_y and ϵ_z follows the same trend as before but there is significant change in the nature of ϵ_x . The value of ϵ_x diminishes exponentially till $t=7$ mm where it reaches zero and the nature of strain changes from tensile to compressive. With further increase in t , ϵ_x remains as compressive with negligible increase in its magnitude. The compressive stress, ϵ_y , persists for all the coating thicknesses while its magnitude increases with increase in t . It should be noted that the difference between ϵ_x and ϵ_y remains almost constant irrespective of the value of t .

Figure 4.13. Graph showing the dependence of axial and radial strains on t at center of the FBG for $E=50$ MPa of the pressurizing medium

Figure 4.14 shows the analyses of the case with further reduced value of stiffness, $E=5$ MPa, of the contact plate. ϵ_x , which is initially tensile at low thicknesses, becomes zero at $t=4.5$ mm and remains as compressive for $t > 4.5$ mm. Unlike the previous cases, ϵ_x becomes compressive at a lower value of t . Both ϵ_x and ϵ_y are compressive strains implying that the lateral loading is able to develop an effect similar to axial tensile loading in the FBG. This would be supportive in getting higher values of wavelength shifts during the lateral pressure tuning of FBGs. Comparing Figures 4.12, 4.13 and 4.14, it could be seen that the gap between ϵ_x and ϵ_y is less for low stiffness values of the contact plate. A low stiffness value of the contact plate increases the contact angle, as seen from the analysis in section 4.2.1, and transfers efficiently the force applied in the radial direction to the perpendicular plane. This reduces the difference in the magnitude of strains in the transverse plane. This reduced difference between ϵ_x and ϵ_y reduces the birefringence generated by non-uniform distribution of stresses.

Figure 4.15 shows the comparison of the axial strain (strain in the Z-direction) ε_z , for $E=5, 50$ and 500 MPa, drawn against the thickness t . Contrary to the behavior of radial strains, the axial strain ε_z is tensile for all coating thicknesses. Its magnitude increases with increase in t but the rate of increase drops off as the thickness increases. ε_z is significantly high for $E=5$ MPa and $E=50$ MPa compared to $E=500$ MPa. Therefore it could be seen that lower stiffness of the contact plate is preferred for obtaining higher magnitudes of strains in the axial direction. This is crucial in connection to the requirement of superior axial strain for the center wavelength of the FBG to have improved sensitivity to the applied lateral load. Since the plane of the gratings is perpendicular to the axis of the fiber only the axial strain ε_z can produce maximum gradient in the period of the grating during strain tuning.

Figure 4.14. Graph showing the dependence of axial and radial strains on t at the center of the FBG for $E=5$ MPa of the pressurizing medium

Figure 4.15. Comparison of axial strains for $E=5$, 50 and 500 MPa ($\mu=0.3$)

4.3.3 Analysis of wavelength response

The influence of thickness t on the spectral response for $E=5$, 50 and 500 MPa of the pressurizing medium is summarized in Figures 4.16, 4.17, and 4.18. The wavelength shift for both X and Y polarizations $\Delta\lambda_x$ and $\Delta\lambda_y$ increase with increase in t . The rate of increase is high at lower values of t , but drops down as t increases. The difference between the values of $\Delta\lambda_x$ and $\Delta\lambda_y$, which is responsible for the birefringence, is also shown in the graph. It could be seen that the difference is negligible compared to the absolute values of shifts and cannot cause any apparent peak splitting. A comparison of the effect of stiffness is made in Figure 4.19. For $E=5$ MPa, the shift in the center wavelength is 0.44 nm for $t=5$ mm which increases up to 0.58 nm for $t=10$ mm where as for $E=50$ MPa, it is only 0.38 nm and 0.47 nm, respectively. The wavelength shift sensitivity for $E=500$ MPa is extremely low and the maximum value of peak shift achievable is 0.18 nm as can be seen from the graph.

Figure 4.16 Graph showing the wavelength shift with respect to t for X and Y polarizations, $\Delta\lambda_x$ and $\Delta\lambda_y$ for $E=5$ MPa ($\mu=0.3$)

Figure 4.17 Graph showing the wavelength shift with respect to t for X and Y polarizations, $\Delta\lambda_x$ and $\Delta\lambda_y$ for $E=50$ MPa ($\mu=0.3$)

Figure 4.18. Graph showing the wavelength shift with respect to t for X and Y polarizations, $\Delta\lambda_x$ and $\Delta\lambda_y$ for $E=500$ MPa ($\mu=0.3$)

Figure 4.19. Comparison of the wavelength shift sensitivities $\Delta\lambda_x$ for $E=5$, 50 and 500 MPa ($\mu=0.3$)

From Fig. 4.19, it could also be noted the wavelength shift trends with respect to t for various E values is similar to dependence of axial strain on t as can be inferred from a comparison with Fig. 4.15. Therefore it can be concluded that it is the axial strain which

is mainly responsible for wavelength shift. Thus the idea of enhancing the sensitivity to transverse loading is to transfer as much possible the transverse loading to axial strain.

4.3.4 Birefringence

A comparison of the birefringence developed due to non-uniform distribution of stress across the cross section of the fiber for different E values of the pressurizing medium is shown in Figure 4.20. For $E=5$ MPa, $(\Delta\lambda_x - \Delta\lambda_y)$ amounts to 0.027 nm at lower values of t but increases to 0.031 nm for $t=10$ mm. Similarly for $E=50$ MPa, the value of $(\Delta\lambda_x - \Delta\lambda_y)$ lies between 0.033 nm and 0.036 nm whereas it is relatively high for $E=500$ MPa. For $E=500$ MPa, $(\Delta\lambda_x - \Delta\lambda_y)$ increases from 0.031 nm to 0.039 nm in the thickness range 1-4 mm and remains almost constant for further increase in the value of t .

Figure 4.20. Graph showing the effect of thickness and material stiffness on birefringence ($\mu=0.3$, $P=5$ N)

From these analyses, it is clear that a contact plate with low stiffness is very effective in distributing the applied lateral load through a larger contact angle and making it more hydrostatic in nature. This leads to insignificant birefringence due to the near-uniform distribution of stresses in the radial directions. Also the hydrostatic nature of the applied load enhances the axial strain, which in turn increases the sensitivity of wavelength shift to the applied lateral load.

4.3.5 Effect of contact friction

Since the applied lateral load is transferred to the fiber through the area in contact, the prevailing contact conditions are important. The influence of the contact conditions between the fiber and the plate on the wavelength shift sensitivity is evaluated by varying the contact friction.

Figure 4.21. Graph showing the effect of contact friction on the wavelength shift sensitivity for varying plate thickness

From Figure 4.21, which shows the effect of the coefficient of friction for $E=5$ MPa of the contact plate, it can be seen that a higher contact friction is helpful in

enhancing the wavelength shift during lateral pressure tuning. For example, for $t=5$ mm, the wavelength shift is only 0.24 nm for $\mu=0.1$, where as it is 0.44 nm and 0.49 nm for $\mu=0.3$ and $\mu=0.5$, respectively. At higher contact friction, it can be possible to transfer the applied load efficiently to the fiber without slipping. A frictional coefficient of $\mu=0.1$ is insufficient to cause the maximum possible wavelength shift for a given configuration, as seen from the graph. Due to low friction, the surface bondage between the fiber and the plate is deficient to cause the deformation of the fiber.

4.4 EXPERIMENTAL ANALYSIS

4.4.1 Experimental setup

Fiber Bragg gratings, with 10 mm length, were written on SMF-28 using the standard phase mask technique using a frequency doubled argon ion laser at 244 nm. The experimental setup and the loading geometry is shown in Figure 4.22. The contact medium having the specified thickness is attached to a ceramic plate and the fiber containing the grating is compressed between the plates. A reference fiber is used to balance the load. Therefore it should be noted that only half of the applied load is acting on the FBG. A broadband tunable light source and an optical spectrum analyzer (ANDO: AQ-6317B with resolution of 0.01 nm) were used for online analysis of the reflection spectrum. All the experiments were done at a constant room temperature.

Figure 4.22. Experimental set up and loading geometry

4.4.2 Results and Discussion

Figure 4.23 shows the wavelength shift with respect to the applied load, for a silicone rubber contact material with $E=20$ MPa and $\mu \cong 0.1$, at various thickness values. A thin layer of resin, whose friction coefficient with silica fiber is approximately 0.1, was attached to the contact medium to establish same contact friction for all the materials and thereby to compare the influence of material property. A linear relationship with the load can be fitted with the experimental results as can be seen from the graph. Repeated loading and unloading was carried out and the error amounts to 0.05 nm. Mean deviation was taken to get the error bars.

The dotted lines represent the numerical simulation results based on equations. Minor deviations from the experimental results can be observed. The discrepancy is more prominent at higher loads and higher coating thickness. However the simulation results lies almost within the experimental error limit. The variation is

expectedly due to the viscoelastic nature of the polymer material. (which is analyzed in detail in chapter 5, section 5.4). The maximum deviation from the linear fit of experimental results is 0.06 nm for $t=10$ mm for a load of 20 N, which is 5 % of the absolute value.

Figure 4.23. Wavelength shift with respect to load for a silicone rubber contact material with $E=20$ MPa and $\mu \cong 0.1$. The dotted lines represent the numerical simulation results.

The effect of thickness of the contact medium, for an applied load of 5 N, for three types of medium material is summarized in Figure 4.24. Similar trends as obtained from the FE analysis can be seen from the graph. Significant enhancement in sensitivity in sensitivity can be achieved by increasing the thickness t as can be seen from the graph. However the rate of increase in sensitivity drops down with increase in t .

The influence of the stiffness of the contact material can also be analyzed from the graph. It could be seen that a contact medium with low stiffness highly enhances the sensitivity, confirming the results obtained from finite element analysis. For example, a 5 mm thick hard plastic contact medium gives a shift of only 0.09 nm for a load of 5 N whereas a low stiff silicone rubber can give wavelength shift as high as 0.21 nm. Also it can be inferred that the sensitivity cannot be increased significantly by increasing t for the contact medium with high stiffness.

Figure 4.24. Graph showing the effect of thickness of the contact medium at contact friction $\mu \cong 0.1$. The dotted lines represent the numerical simulation results.

The numerical simulation results based on equations are also incorporated in Fig. 4.24 for comparison. For $E=20$ MPa and for $E=65$ MPa, the maximum discrepancy is only 0.02 nm (at $t=8$, $E=20$ MPa), while for $E=700$ MPa, it is 0.03 nm (at $t=8$ mm).

To study the effect of an increase the contact friction, the bare fiber is coated with a thin film having thickness 5 μ m. As in the previous case, a thin layer of leather is attached to the contact plate such that the contact friction between the coated fiber and contact medium is approximately 0.5. The wavelength shift with respect to the applied load for such a configuration is shown in Figure 4.25. Comparing with Figure 4.23, it could be seen that the frictional effect is significant. For example, for a load of 25 N and for $t=2$ mm, increasing the frictional coefficient from 0.1 to 0.5 could increase the shift value from 0.32 nm to 1.16 nm.

The numerical simulation results corroborate with the experimental results as can be seen the dotted lines in Fig. 4.25. The maximum discrepancy from experimental value is 0.07 nm, occurs at $t=10$ mm and $P=20$ N, which is only 3.2 % of the absolute value.

Figure 4.25. Wavelength shift with respect to load for a silicone rubber contact material with $E=20$ MPa and $\mu \cong 0.5$. The dotted lines represent the numerical simulation results.

The effect of thickness of the contact medium for the higher value of contact friction is summarized in Figure 4.26. Comparing with Figure 4.24, it could be seen that the influence of friction is not so apparent for the contact medium with high stiffness. The wavelength shift values for the hard plastic contact medium with $E=700$ MPa remains approximately at 0.1 nm irrespective of the contact friction. Also it could be seen from Figure 4.20 that increasing the thickness of the high stiff contact medium does not have any effect in enhancing the wavelength shift sensitivity. Therefore it could be concluded that the sensitivity enhancement effects of higher thickness and high contact friction exists only for contact medium with low stiffness values.

Figure 4.26. Graph showing the effect of thickness of the contact medium at contact friction $\mu \cong 0.5$. The dotted lines represent the numerical simulation results.

The numerical simulation results agrees well with the experimental ones for $E=20$ MPa and $E=65$ MPa. However for $E=700$ MPa, deviations as high as 0.03 (30 %) nm can be observed which is significantly high. However it can be noted that wavelength shift

sensitivity and the total shift in this case is very less (0.1 nm) and therefore the chance for experimental error is more in this case.

4.5 SUMMARY

The effect of contact medium on the lateral pressure tuning of fiber Bragg gratings is investigated for the first time and presented in this chapter. The dependence of the contact area between the fiber and the contact medium on the applied load and the material properties is characterized. Theoretical formulations have been carried out for the stress distribution in the FBG at different contact medium configurations. Expressions for stress induced refractive index modulation and the associated mode coupling are modified, taking the effects of contact medium material and contact area into consideration. Finite element analysis has been carried out to characterize the influence of contact medium on the stress distribution and the wavelength response nature of the FBG subjected to LPT. Experimental studies have also been conducted. The major results and findings can be summarized as follows.

- It is found that the contact area between the fiber and the pressurizing medium is dependent on the stiffness of the contact medium. A large contact area is helpful in effective transfer of the applied lateral load to the fiber containing the grating. Thus the load transfer is more hydrostatic in nature and enhances the lateral pressure sensitivity and the wavelength tuning range and reduces birefringence.
- The influence of the stiffness of the contact plate on the wavelength shift sensitivity is characterized and it is found that a pressurizing medium with low value of Young's modulus is advantageous for higher sensitivity.

- Again, the wavelength shift sensitivity is highly dependent on the thickness of the medium of external perturbation. The sensitivity increases with increase in the thickness value, the rate of increase being higher at lower values. An optimum thickness, which depends on the contact material and the contact conditions, has to be selected for enhancing the sensitivity.
- Also it is found from the analysis that a higher contact friction between the fiber and the contact medium is favorable for effective transfer of the load and thereby for improved sensitivities.
- The difference in the stress gradient in the perpendicular X and Y directions can be effectively reduced by selecting a suitable contact medium with low Young's modulus.
- At lower values of thickness the birefringence developed due to non-uniform distribution of stresses increases with increase in contact medium thickness. Therefore special care has to be taken while selecting these values for achieving enhanced sensitivities.
- The similarity between dependence of axial strain and wavelength shift on thickness t in Fig. 4.15 and 4.19 indicates that the axial strain is mainly responsible for the wavelength shift. So the idea of enhancing sensitivity must be focused on enhancing the axial strain.

Chapter 5

ENHANCED LATERAL PRESSURE TUNING BY POLYMER PACKAGING

5.1 INTRODUCTION

Polymer coating on FBG is proposed to enhance the performance of wavelength tuning through lateral loading, the analysis of which is presented in this chapter. The effect of coating material properties and the coating thickness is evaluated. Theoretical analysis is carried out to evaluate the stress distribution and thereby to compute the photo-elastically induced refractive index modulation and the associated wavelength shift. Theoretical formulation is followed by finite element analysis and experimental studies. Analysis of the effect of polymer coating on the hydrostatic pressure tuning (HPT) of FBGs is also carried out and described in this chapter.

5.2 ANALYSIS OF POLYMER COATED FBG UNDER LATERAL PRESSURE

The schematic of the polymer coated FBG is shown in Figure 5.1 in which the fiber containing the grating is coated uniformly with a suitable polymer. The length of the coating is taken as equal to the length of the grating for the analysis.

Figure 5.1 Schematic of polymer coated FBG

5.2.1 Contact length

The length of contact $2a$ between the contact medium and the polymer coated FBG during the lateral compression process, as shown in Figure 5.2, can be calculated by the relation [112, 113]

$$2a = 4 \left[\frac{PR_c}{\pi L} \frac{(1 - \nu_2^2)E_c + (1 - \nu_c^2)E_2}{E_c E_2} \right]^{1/2} \quad (5.1)$$

E_c and ν_c respectively represents the Young's modulus and Poisson's ratio of the coating material. R_c represents the outer radius of the coated FBG. Figure 5.3 shows the influence of the coating material stiffness E_c and the coating thickness on the contact length. The length of the FBG under consideration is 10 mm and the graph is plotted for an applied lateral load $P=5$ N. The value of Poisson's ratio ν_c of the polymer coating assumed for the analysis is 0.45. The compressing plate is considered as rigid medium (steel plate) with $E=200$ GPa and $\nu=0.30$. The presence of glass fiber is neglected as the contact is solely between the contact medium and the polymer coating.

Figure 5.2. Schematic of coated FBG subjected to LPT. P is the applied load and $2a$ is the contact length.

Figure 5.3. Graph showing the influence of the coating material stiffness and the coating thickness on the contact length (R_c = radius of the coated FBG)

It can be seen that the contact length $2a$ falls exponentially with increase in E value. For example, the contact length is only 0.06 mm for a coating radius of 1 mm if $E_c=500$ MPa. Where as its value is increased by almost 10 times for $E_c=5$ MPa. Similar trend can be observed for all the values of the coating radius. Also a higher value of coating thickness imparts large contact area as can be seen from the graph.

5.2.2 Stress distribution and refractive index modulation

When the coated FBG is compressed laterally as shown in Figure 5.2, the contact pressure distribution ' p_0 ' along the contact area is uniform and can be expressed as,

$$p_0 = \frac{P}{2aL} \quad (5.2)$$

where $2a$ is the contact length and L is the length of the FBG under consideration.

Referring to Figure 5.4, the load dP , acting in the radial direction on an elemental strip subtending an angle $d\theta_1$ at an angular distance θ_1 from the Y axis

$$dP = p_0 \cos \theta_1 (R + t) d\theta_1 L \quad (5.3)$$

$$dP = \frac{P}{2a} \cos \theta_1 (R + t) d\theta_1 \quad (5.4)$$

t is the coating thickness and R , the radius of the uncoated FBG. Utilizing equations A.8 and A.9 from Appendix A, the stresses at any point (r, θ) due to this elemental force dP can be obtained as follows.

$$d\sigma_x(r, \theta) = \frac{-2 \left(\frac{P}{2a} \cos \theta_1 (R + t) d\theta_1 \right)}{\pi L} \left\{ \frac{r^2 \cos^2(\theta - \theta_1) (R + r \sin(\theta - \theta_1))}{[r^2 + R^2 + 2Rr \sin(\theta - \theta_1)]^2} + \frac{r^2 \cos^2(\theta - \theta_1) (R - r \sin(\theta - \theta_1))}{[r^2 + R^2 - 2Rr \sin(\theta - \theta_1)]^2} - \frac{1}{2R} \right\} \quad (5.5)$$

$$d\sigma_y(r, \theta) = -\frac{2\left(\frac{P}{2a} \cos \theta_1 (R+t) d\theta_1\right)}{\pi L} \left\{ \frac{(R+r\sin(\theta-\theta_1))^3}{[r^2 + R^2 + 2Rr\sin(\theta-\theta_1)]^2} + \frac{(R-r\sin(\theta-\theta_1))^3}{[r^2 + R^2 - 2Rr\sin(\theta-\theta_1)]^2} - \frac{1}{2R} \right\} \quad (5.6)$$

Figure 5.4. Schematic of the load dP , acting in the radial direction on an elemental strip subtending an angle $d\theta_1$ at an angular distance θ_1 from the Y axis

Therefore the total stresses can be found out by integrating equations 5.5 and 5.6 over the contact length $2a$.

$$\sigma_x(r, \theta) = \int_{-\phi}^{+\phi} \frac{-2\left(\frac{P}{2a} \cos \theta_1 (R+t) d\theta_1\right)}{\pi L} \left\{ \frac{r^2 \cos^2(\theta-\theta_1)(R+r\sin(\theta-\theta_1))}{[r^2 + R^2 + 2Rr\sin(\theta-\theta_1)]^2} + \frac{r^2 \cos^2(\theta-\theta_1)(R-r\sin(\theta-\theta_1))}{[r^2 + R^2 - 2Rr\sin(\theta-\theta_1)]^2} - \frac{1}{2R} \right\} \quad (5.7)$$

$$\sigma_y(r, \theta) = \int_{-\phi}^{+\phi} \frac{2 \left(\frac{P}{2a} \cos \theta_1 (R+t) d\theta_1 \right)}{\pi L} \left\{ \frac{(R+r \sin(\theta-\theta_1))^3}{[r^2 + R^2 + 2Rr \sin(\theta-\theta_1)]^2} + \frac{(R-r \sin(\theta-\theta_1))^3}{[r^2 + R^2 - 2Rr \sin(\theta-\theta_1)]^2} - \frac{1}{2R} \right\} \quad (5.8)$$

where $\cos \phi = \frac{a}{R+t}$ and the axial strain at any point (r, θ) is,

$$\sigma_z(r, \theta) = -\nu [\sigma_x(r, \theta) + \sigma_y(r, \theta)] \quad (5.9)$$

Since the axial stress varies as a function of r and θ , we need to integrate it over the cross sectional area of the FBG to determine the net axial load and the associated strain.

Consider an elemental area on the cross section of the coated FBG, as shown in Figure

5.5, of thickness dr at a distance of r from the center subtending an angle $d\theta$.

Figure 5.5 Schematic of the elemental area of thickness dr at a distance of r from the center subtending an angle $d\theta$ on the cross section of the coated FBG

The elemental area dA is,

$$dA = \frac{[rd\theta + (r+dr)d\theta]}{2} dr \quad (5.10)$$

Neglecting the $(dr)^2 d\theta$ term,

$$dA = rd\theta dr \quad (5.11)$$

The axial load dP_z acting on this area is,

$$dP_z = \sigma_z(r, \theta) dA = \sigma_z(r, \theta) r d\theta dr \quad (5.12)$$

The net axial load P_z can be found out by integrating dP_z over the cross sectional area.

$$P_z = \int_A \sigma_z(r, \theta) dA = \int_0^{R+t} \int_0^{2\pi} \sigma_z(r, \theta) r d\theta dr \quad (5.13)$$

This can be expressed as

$$P_z = E_f \varepsilon_f A_f + E_c \varepsilon_c A_c \quad (5.14)$$

where ε_f , ε_c represents the axial strains and A_f , A_c represents the cross sectional area of the fiber and the coating respectively. If the coated FBG is assumed as a composite, the axial strains ε_f and ε_c are equal. But it makes sense if we incorporate a factor ψ to explain the extent of adhesion or degree of bonding between the coating and the fiber.

$$\varepsilon_f = \psi \varepsilon_c \quad (5.15)$$

$$\therefore P_z = \varepsilon_f \left(E_f A_f + \frac{E_c A_c}{\psi} \right) \quad (5.16)$$

Once the stress distribution is known, the refractive index modulation and the coupling coefficient for X and Y polarizations can be calculated by the equations 4.23 to 4.28. Thus the change in the spectral response of the coated FBG due to the applied lateral load can be computed according to equations 3.20 to 3.27.

5.3 FINITE ELEMENT ANALYSIS

Finite element analysis is carried out to characterize the dependence of the coating thickness and the stiffness of the polymer on the peak wavelength shift sensitivity and birefringence.

5.3.1 Loading Geometry, boundary conditions & coordinate system

The single mode silica fiber, in which uniform gratings were written, is considered as an isotropic medium with insignificant intrinsic birefringence. Photoelastic and mechanical properties are assumed to be the same for the core and the cladding. The schematic of the model and the co-ordinate system used is shown in Figure 5.6. The central longitudinal axis of the fiber is designated as Z and the loading direction is along Y-axis. The applied boundary conditions are as follows. For the plate, the degree of freedom is arrested in the Z and X directions, allowing it to move only in the loading direction. For example, for the plate, $U_x=0$, $U_z=0$. For the fiber, both the ends are unconstrained while the degree of freedom is arrested in the Z direction at the center of the fiber ($U_z=0$ at $z = L/2$). The displacement of all the nodes in the X-Z plane at $y = 0$ is arrested in the Y-direction ($U_y=0$ at $y = 0$). Plane stress conditions ($\sigma_z=0$) are assumed since both the ends of the fiber are unconstrained and there is significant strain in the Z-direction.

Figure 5.6. Schematic of the FE model and the co-ordinate system

The change in the Bragg center wavelength for the X and Y polarizations, $\Delta\lambda_{Bx}$ and $\Delta\lambda_{By}$, due to an applied stress field is calculated as per equations 4.31 and 4.32. The same properties of the FBG as mentioned in section 4.3.1 are used for the analysis.

The length of the FBG under consideration is 10 mm. The wavelength shift due to the applied lateral load is calculated with respect to a center wavelength of 1550 nm.

5.3.2 Analysis of axial and radial strains

Figure 5.7 shows the radial strains at the axis of the FBG at various coating thicknesses, under an applied lateral load of 0.5 N/mm. The stiffness of the polymer is taken as 5 MPa. It could be seen that ε_y , the strain in the loading Y-direction is compressive in nature whereas the strain in the perpendicular radial direction ε_x is tensile. Since the nature of the applied load in the Y-direction is compressive, it is quite obvious that the corresponding strain ε_y remains compressive all the time irrespective of the magnitude of the applied load or the coating thickness. As the coating becomes thicker, the magnitudes of both ε_x and ε_y decrease, as can be seen from the Figure 5.7. The value of ε_x diminishes exponentially till the coating diameter of $D=1500 \mu\text{m}$ where it reaches zero and the nature of strain changes from tensile to compressive. With further increase in D , ε_x remains compressive with negligible increase in its magnitude. The compressive stress, ε_y , persists for all the coating thicknesses while its magnitude decreases with increase in diameter to as high as $D=1000 \mu\text{m}$. The rate of drop off reduces for further increase in coating diameter and finally the value of strain reaches a constant value of -1.2×10^{-5} .

Figure 5.7. Radial strains at center of the FBG for a lateral load of 0.5 N/mm

It could also be seen that at lower coating thicknesses ($D < 300 \mu\text{m}$), the ϵ_x and ϵ_y differ, whose magnitude decreases monotonically with increase in coating thickness. The polymer coating, having very low stiffness, transfers efficiently the force applied in the radial direction to the perpendicular plane and thereby reduces the difference in the strains in the transverse plane. This reduced difference between ϵ_x and ϵ_y at higher coating thicknesses reduces the birefringence generated by non-uniform distribution of stresses. For diameters above $1500 \mu\text{m}$, both ϵ_x and ϵ_y are compressive strains implying that the lateral loading is able to develop an effect corresponding to axial tensile strain in the FBG. This would be supportive in getting higher values of wavelength shifts during the lateral pressure tuning of FBGs.

Figure 5.8. Graph showing the variation of axial strain with coating diameter at different loads P

Figure 5.8 shows the plot of the axial strain (strain in the Z-direction) ϵ_z , for lateral loads of 0.5, 1.0 and 1.5 N/mm, drawn against coating thickness. Contrary to the behavior of radial strains, the axial strain, which is tensile for all coating thicknesses, increases as the coating thickness increases. The magnitude of ϵ_z increases with coating thickness till $D=3000 \mu\text{m}$ and then decreases at larger D . Therefore, this 3000 μm coating diameter represents the optimum value for maximum axial strain. This is crucial in connection to the requirement of superior axial strain for the center wavelength of the FBG to have improved sensitivity to the applied lateral load. Further, at this coating thickness, it could be noted that the magnitudes of radial strains are negligible compared to the axial strain. Since the plane of the gratings is perpendicular to the axis of the fiber only the axial strain ϵ_z can produce maximum gradient in the period of the grating during strain tuning.

5.3.3 Analysis of wavelength shift upon lateral load

5.3.3.1 Wavelength shift sensitivity

Figure 5.9 is the shift in the center wavelength of the FBG plotted against the coating thickness for applied lateral loads of 0.5, 1.0 and 1.5 N/mm. The shift in the X-polarization $\Delta\lambda_x$ was chosen for plotting this graph. It could be seen that the curves are almost linear till $D = 1000\ \mu\text{m}$ beyond which the slope gradually decreases. Maximum peak shift for all the loads are obtained at $D= 2500\text{--}3000\ \mu\text{m}$ range. Increasing the coating diameter beyond this range reduces the peak shift. Therefore, for the polymeric coating under consideration with $E=5\times10^6\ \text{N/m}^2$, the diameter range of 2500-3000 μm could be identified for optimal peak-shift sensitivity of FBG to the applied lateral load. At this diameter range, the peak shift sensitivity is 0.804 nm/N/mm.

Figure 5.9. Shift in the Bragg center wavelength with respect to coating thickness for various loads.

5.3.3.2 Effect of coating stiffness

Figure 5.10, which shows the shift in the Bragg center wavelength under a lateral load of 0.5 N/mm for three types of polymers, summarizes the effect of the polymeric coating on the FBG. The Poisson’s ratio of all the polymers was assumed to be 0.45. It could be seen that the peak shift is maximum for the polymer with Young’s modulus $E=5\times10^6$ N/m². The peak shift sensitivity decreases as the stiffness of the polymeric coating increases. Also it could be seen that there exists an optimum value of coating thickness at which the peak shift is maximum. For a lateral load of 0.5 N/mm, the maximum value of shift attainable for polymer with $E=5\times10^6$ N/m² is 0.40 nm while it is only 0.34 nm and 0.185 nm respectively for the polymers with $E=50\times10^6$ N/m² and $E=500\times10^6$ N/m². Therefore the polymer with $E=5\times10^6$ N/m² is selected for all the further analysis.

Figure 5.10. Effect of stiffness of the polymer and the coating thickness on the peak shift sensitivity for an applied lateral load of 0.5 N/mm

5.3.3.2 Birefringence

To evaluate the birefringence effects at low coating thickness values, the shift in the X and Y polarizations $\Delta\lambda_x$ and $\Delta\lambda_y$ are plotted together in Figure 5.11 for an applied lateral load of 0.5 N/mm. The peak shifts for both X-polarization and Y-polarization increase with increase in coating thickness while the difference between the values of $\Delta\lambda_x$ and $\Delta\lambda_y$ reduces. For a bare FBG and at lower coating thickness, the difference is very prominent compared to the absolute value of shift. For example, for $D=150\text{ }\mu\text{m}$, $\Delta\lambda_x$ is 0.05 nm and $\Delta\lambda_y$ is 0.02 nm and the difference between $\Delta\lambda_x$ and $\Delta\lambda_y$ amounts to 0.03 nm which is 150 % of the absolute value of $\Delta\lambda_y$. However it could be seen that $\Delta\lambda_x$ and $\Delta\lambda_y$ are getting closer with increasing coating thickness and almost merges at $D=2000\text{ }\mu\text{m}$.

Figure 5.11. Graph showing the shift in the X and Y polarizations $\Delta\lambda_x$ and $\Delta\lambda_y$ at low coating thicknesses for an applied lateral load of 0.5 N/mm

Figure 5.12. Graph showing the effect of coating thickness on the peak split ($\Delta\lambda_x - \Delta\lambda_y$) for various loads.

The difference between the values of the shift in the X and Y polarizations $\Delta\lambda_x$ and $\Delta\lambda_y$ at various loads is summarized in Figure 5.12. The difference is more prominent for bare fiber and for lower coating thicknesses and decreases exponentially with increase in coating thickness. However the rate of drop off reduces at higher coating thicknesses. For example, for a lateral load of 0.5 N/mm, the difference is 0.04 nm for an uncoated FBG which reduces to 0.028 nm and to 0.01 nm with coating thicknesses of 145 μm and 500 μm respectively. For diameters above 1000 μm, the difference is less than 0.01 nm, which is very insignificant and cannot cause any apparent peak splitting. It can be thus concluded that the birefringence created due to the difference in the wavelength shifts between the X and Y polarization can be reduced to an insignificant level by an appropriate choice of coating thickness.

Figure 5.13. Graph showing the effect of coating thickness and the stiffness of the polymer coating on the birefringence for an applied lateral load of 0.5 N/mm

The effect of the stiffness of the polymer coating on the birefringence is summarized in Figure 5.13. The curves are almost overlapping and follow the same trend from which it can be concluded that the coating material property does not significantly affect the birefringence. And therefore the birefringence could be conveniently expressed as a function of coating thickness alone.

5.4 EXPERIMENTAL ANALYSIS

The coated FBG with 10 mm length is held between two smooth ceramic plates as shown in Figure 5.14. A similar dummy cylinder, with the same geometry and material, is used to balance the load applied from the top, as shown in the figure. A broadband tunable light source and an optical spectrum analyzer (ANDO: AQ-6315B with

resolution of 0.05 nm) are used for online analysis of the reflection spectrum. All the experiments are done at a constant room temperature.

Figure 5.14. Experimental setup

To analyze the effect of viscoelasticity of the polymer, the transient response of the polymer packaged FBG is studied under various loads and shown graphically in Figure 5.15. The polymer under consideration is silicone rubber and the diameter D is 4000 μm . The wavelength shift is plotted as a function of time. The wavelength rises abruptly upon loading, which gradually decays and comes to constant level within a few seconds. For example, for a test load of 1.63 N/mm, the wavelength shifts by 0.516 nm upon loading but drops backs to 0.432 nm after 5 seconds and remains constant. Similar behavior is observed for all other loads. Therefore, in order to get reliable measurements, readings are taken at least 5 seconds after the application of the load.

Figure 5.15. Graph showing the transient response of the polymer coated FBG at various loads

Figure 5.16. Peak shift with respect to applied load for a coating diameter $D=4000\text{ }\mu\text{m}$, $E=5\text{ MPa}$

The shift in the central peak wavelength with respect to the load for a diameter $D=4000\text{ }\mu\text{m}$ is plotted in Figure 5.16. Repeated loading and unloading was done and the curve represents the linear fit of the experimental results. The maximum deviation from linearity is 0.034 nm . At this coating diameter, the sensitivity of the shift in peak center to the applied load is 0.3 nm/N/mm . This is almost 7 times as high as the lateral pressure sensitivity of bare fiber. The peak split sensitivity of bare FBG, subjected to lateral loading is also shown in Figure 5.16 for comparison. The experimental results are in good agreement with the finite element simulation. Minor variations could be due to the non-linearity in the change in the contact area between the polymer and the plate at successive load steps. The dotted line represents the numerical simulation results using equations 5.1, 5.7, 5.8, 5.13 developed in section 5.2 and Eqns. 4.23 and 4.24. The values are slightly higher than both experimental and FEM results, especially at higher loads. The finite element simulation is more close to experimental results.

Figure 5.17. Graph showing the effect of coating thickness on the wavelength shift, $E=5\text{ MPa}$. The dotted lines represent the numerical simulation results.

The effect of coating diameter on the wavelength shift sensitivity is summarized in Figure 5.17. It could be seen that there exists an optimum diameter at which the wavelength shift sensitivity is maximum, confirming the results obtained from finite element analysis. The wavelength shift increases with increase in coating diameter till $D \cong 2.5\text{-}3.5$ mm. Therefore this could be identified as the optimum range above which the sensitivity slows down. At this diameter range the peak sensitivity is obtained as 0.35 nm/N/mm. The dotted lines represent the numerical simulation results using equations 5.1, 5.7, 5.8, 5.13 developed in section 5.2 and Eqns. 4.23 and 4.24. Slight discrepancy exists, especially at higher loads and large coating diameter. It could be due to the viscoelastic nature of the polymeric material, which is more prominent at higher loads and large coating diameter.

5.5 ANALYSIS OF WAVELENGTH TUNING BY HYDROSTATIC PRESSURE

Analysis of hydrostatic pressure tuning (HPT) of polymer coated FBG is presented in this section. Even though the gain in hydrostatic pressure sensitivity of coated FBG has been reported previously [28], no comprehensive study regarding the influence of coating material or the coating thickness was done. Therefore the effect of coating material properties and the coating thickness is evaluated here in detail. Theoretical analysis is carried out to evaluate the stress distribution and thereby to compute the photo-elastically induced refractive index modulation and the associated wavelength shift.

5.5.1 Design of coated FBG for enhanced HPT

Consider the coated FBG subjected to a hydrostatic pressure P_0 as shown in Figure 5.18. Let ' t ' be the coating thickness and L the length of the FBG. The radial and circumferential stress at position (r, θ) can be written as [112],

$$\sigma_r(r, \theta) = -P_0 \quad (5.17)$$

$$\sigma_\theta(r, \theta) = -P_0 \quad (5.18)$$

Figure 5.18 Schematic of the coated FBG subjected to axially unconstrained radial pressure

It can be seen that the stresses are independent of r and θ owing to the symmetry of the problem. And the axial stress σ_z is,

$$\sigma_z(r, \theta) = -\nu(\sigma_r(r, \theta) + \sigma_\theta(r, \theta)) \quad (5.19)$$

The net axial force developed due to the combined stresses in the fiber and the coating can be expressed as,

$$P_z = \sigma_{zc} A_c + \sigma_{zf} A_f \quad (5.20)$$

where σ_{zc} and σ_{zf} represents the axial stresses in the coating and fiber respectively. A_c and A_f represents the cross sectional area of the coating and fiber. P_z can be also written in the form,

$$P_z = E_f \varepsilon_f A_f + E_c \varepsilon_c A_c \quad (5.21)$$

where ε_f and ε_c represents the axial strain in the fiber and the coating, respectively. Considering the coated FBG as a composite, the axial strains ε_f and ε_c should be the same. A factor ψ is used to explain the extent of adhesion between the coating and the fiber according to

$$\varepsilon_f = \psi \varepsilon_c \quad (5.22)$$

$$\therefore P_z = \varepsilon_f \left(E_f A_f + \frac{E_c A_c}{\psi} \right) \quad (5.23)$$

P_z , as obtained from Equation 5.20, can be substituted in Equation 5.23 to get the axial strain developed in the fiber. Upon simplification, ε_f can be obtained as

$$\varepsilon_f = \frac{2P_0 (\nu_c A_c + \nu_f A_f)}{\left(E_f A_f + \frac{E_c A_c}{\psi} \right)} \quad (5.24)$$

From Equation 5.24, it could be noticed that a high Poisson's ratio value ν_c of the coating enhances the axial strain ε_f in the fiber. Also a relatively high cross sectional area (or thickness) and a low value of E_c enhances the axial strain.

5.5.2 Finite element characterization

5.5.2.1 FE model and boundary conditions

The schematic of the FE model of the coated FBG subjected to hydrostatic pressure is shown in Figure 5.19. The applied boundary conditions: degree of freedom of all nodes along the Z-axis at $x=0$ and $y=0$ is arrested in the X and Y directions. i.e., at $x=0$; $y=0$, $U_x=0$; $U_y=0$. The pressure is applied in the radial direction over the length L of the FBG as shown in the figure.

Figure 5.19 Schematic of the FE model and boundary conditions

5.5.2.2 Discussion

The shift in the center wavelength of a bare FBG with respect to the hydrostatic pressure is plotted in Figure 5.20. It could be seen that the shift is linear as expected, similar to the case of axial stretching. The peak shift sensitivity is calculated as 0.012 nm/MPa. It could be noted that sensitivity is meager and a very high hydrostatic pressure is needed to produce a noticeable shift in the spectral response.

Figure 5.20 Wavelength shift due to hydrostatic pressure on bare FBG

a) $E=500\text{ MPa}$

The influence of the coating diameter on the peak shift sensitivity of FBG subjected to HPT for $E=500\text{ MPa}$ of the coating material is summarized in Figure 5.21. It could be seen that a higher coating thickness is favorable for improved sensitivities. The peak shift sensitivity increases exponentially till $D=2000\text{ }\mu\text{m}$ above which the rate of increase gradually slows down. A coating diameter above $D=3000\text{ }\mu\text{m}$ produces negligible increase in the peak shift values. Therefore a coating thickness in the range $2000\text{-}3000\text{ }\mu\text{m}$ can be identified as the optimum range for a material with $E=500\text{ MPa}$.

Figure 5.21 Graph showing the effect of coating diameter on the peak shift sensitivity of FBG subjected to HPT for $E=500\text{ MPa}$ of the coating material

b) $E=50\text{ MPa}$

Compared to the previous case with $E=500\text{ MPa}$, significant enhancement in the spectral shift is observed if E of the coating material is further reduced. For $D=5000\text{ }\mu\text{m}$, a spectral shift of more than 4 nm is attainable for a hydrostatic pressure of 1 MPa .

The dependence of the coating diameter D for $E=50$ MPa of the coating material is summarized in Figure 5.22. Similar to the previous case, the peak shift sensitivity increases with D . The rate of increase is higher at lower values of D and it reduces with increasing D . The calculated peak shift sensitivity values are 0.83, 3.28 and 4.13 nm/MPa for $D=1000$ μm , 3000 μm and 5000 μm , respectively. Therefore for a coating with $D=5000$ μm , hydrostatic pressure changes as low as 0.00242 MPa can be detected with an interrogation system having resolution 0.01 nm.

Figure 5.22 Graph showing the effect of coating diameter on the peak shift sensitivity of FBG subjected to HPT for $E=50$ MPa of the coating material

c) $E=5$ MPa

The influence of hydrostatic pressure on the spectral response of FBG coated with materials having further low values of stiffness is shown in Figure 5.23. It could be noted that the response is similar to the previous cases except that the sensitivity is slightly higher. Spectral shift as high as 5 nm can be noticed for a pressure of 1 MPa.

The peak shift increases with increasing D as can be seen from the graph. For a coating diameter $D=5000\text{ }\mu\text{m}$, the sensitivity is 5.13 nm/MPa and therefore lowest pressure change that can be detected 0.0019 MPa with an interrogation system having resolution 0.01 nm .

Figure 5.23 Graph showing the effect of coating diameter on the peak shift sensitivity of FBG subjected to HPT for $E=5\text{ MPa}$ of the coating material

A comparison of the influence of stiffness of the coating material on the peak shift is shown graphically in Figure 5.24. The graph is plotted for an applied pressure of 0.1 MPa and the peak shift with respect to D is shown for $E=5, 50$ and 500 MPa . It can be seen that considerable enhancement in sensitivity is obtained if E is reduced from 500 to 50 MPa . However, the rate of enhancement is reduced if E is further reduced to 5 MPa . For example, for $D=3000\text{ }\mu\text{m}$, the peak shift value is 0.135 nm for $E=500\text{ MPa}$, while it increases to 0.328 nm for $E=50$ and further increases to 0.388 for $E=5\text{ MPa}$.

Figure 5.24 Graph showing the effect of coating diameter on the peak shift sensitivity of FBG subjected to HPT for $E=50\text{ MPa}$ of the coating material

Thus it has been seen that significant enhancement in sensitivity, compared to the bare fiber, is achievable by the polymeric coating. For a diameter of $D=5000\text{ }\mu\text{m}$, the enhancement is 427.5, 344.2 and 120.8 times for $E=5$, 50 and 500 MPa respectively. The enhancement in the sensitivity could be utilized for a variety of sensing applications in which the pressure is acting hydrostatically. Proper diameter and material property has to be selected depending upon the specific application environment, operating pressure range and the sensitivity requirements.

5.6 SUMMARY

In this chapter, the significance of the effect and function of the proposed polymeric coating technique on the LPT of FBGs is investigated. A suitable coating material with low elastic modulus can effectively transfer the stress, applied in the lateral direction, to

the perpendicular plane, thereby reducing the birefringence and enhancing the lateral pressure sensitivity. Through the analysis, it has been found that the peak wavelength shifts increase with increase in coating thickness. However, there exists an optimum range of coating thickness outside which the peak shift sensitivity decreases. Also the difference between the values of $\Delta\lambda_x$ and $\Delta\lambda_y$ reduces as the coating thickness increases. Therefore a suitable polymeric coating with low stiffness and an optimum value of coating thickness is very effective in enhancing the wavelength sensitivity and in eliminating the stress-induced birefringence during lateral loading. The birefringence can be reduced to an insignificant near zero value and the peak shift sensitivity could be enhanced to almost 10 times as high as that of bare FBG.

The influence of hydrostatic pressure on the wavelength response characteristics of coated FBG was characterized and it was found that considerable enhancement (more than 100 fold) in the sensitivity is achievable by using an optimized polymer coating. The enhancement in the hydrostatic pressure sensitivity has a major impact on the embedded sensing systems. Even a minor change in the properties of the embedding matrix could be monitored through the response of the FBG. This could be effectively utilized for potential structural and biomedical applications.

Chapter 6

ENHANCED WAVELENGTH TUNING BY CLADDING ETCHING

6.1 INTRODUCTION

Cladding etching combined with polymer coating is proposed as a method to enhance the wavelength tuning by axial, lateral, and thermal loading. The effect of cladding etching and subsequent polymer coating on the axial, lateral and thermal tuning performance of FBGs is discussed in this chapter. The proposed cladding etching and polymer coating technique has also been utilized to design an athermal FBG in which the wavelength response is independent of temperature changes.

6.2 AXIAL LOAD SENSITIVITY OF CLADDING ETCHED FBG (CEFBG)

6.2.1 Schematic of CEFBG

It is possible to reduce the tuning force requirements through the reduction of FBG diameter by cladding etching. The schematic of a cladding etched FBG (CEFBG) is shown in Figure 6.1. The silica cladding of the fiber is etched over the length of the grating to the specified depth. The resulting reduction in the diameter increases the stress across the cross section of the fiber during the axial loading process.

Figure 6.1. Schematic of a cladding etched FBG

6.2.2 Experimental analysis

Experimental studies were conducted to analyze the effect of strain and temperature on cladding etched and recoated FBG. For experimental studies, Bragg gratings were written on a photosensitive single mode fiber whose cladding diameter is 125 μm . After the gratings being written, the fiber was etched uniformly along the axis using 40% hydrofluoric acid (HF) solution. The etched FBG was subjected to axial strain and temperature change simultaneously. The axial loading was done in a heating chamber whose temperature can be varied with an accuracy level of 1 $^{\circ}\text{C}$. The transmission spectrum was analyzed online using an optical spectrum analyzer. A comparison of strain and temperature sensitivities of etched and unetched Bragg gratings is given in Figure 6.2. The dotted lines represent the etched FBG and the solid lines represent the unetched one. It could be seen that reducing the cladding diameter considerably increases the wavelength sensitivity to axial loading. The axial load sensitivity of the FBG etched to a diameter of 71 μm is nearly 3 times as that of a bare unetched FBG.

Meanwhile the temperature sensitivity remains the same as expected because the reduction in the cladding diameter has no effect either on the thermooptic effect or on the thermal expansion coefficient of the fiber.

Figure 6.2. Graph showing the axial load and temperature sensitivities of a CEFBG with diameter $d = 85 \mu\text{m}$

The effect of etching depth on the axial load sensitivity is summarized in Figure 6.3. The sensitivity increases exponentially with a decrease in the FBG diameter. For an axial load of 10 gmf, the center wavelength of the unetched FBG shifts by 0.13 nm where as that of FBGs etched to diameters 112, 99, 78, 65 μm shifts respectively by 0.15, 0.19, 0.31 and 0.46 nm as can be seen from the graph.

Figure 6.3. Graph showing the effect of etching depth on the axial load sensitivity

6.3 LATERAL PRESSURE TUNING OF CEFBG WITH POLYMER COATING

The effect of cladding etching on the lateral pressure tuning of polymer coated FBG is evaluated experimentally. The experimental set up and procedure is same as that mentioned in section 5.4. It is found that significant enhancement in the lateral pressure sensitivity of polymer coated FBG could be achieved by etching. The presence of a thick polymer coating prevents the lateral load acting directly on the etched fiber and thereby protects the fiber.

The enhancement in the sensitivity at various etching depths for an FBG coated with 3 mm thick silicone rubber is shown graphically in Figure 6.4. An exponential increase in the wavelength shift could be observed. For a lateral load of 0.41 N/mm, the wavelength shift for unetched FBG is 0.14 nm whereas it is increased to 0.23 nm for an etched FBG diameter 73 μm . For lower loads, the simulation results lies within the error limit of the experimental analysis. However, small discrepancies can be observed

for higher loads, especially for low diameters of etched FBG. The maximum deviation is 0.03 nm which is around 3.3 % of the absolute value. It could be due to visco-elasticity of the polymer coating which becomes more prominent at a reduced diameter (etched to 73 μm) of the FBG.

Figure 6.4. Graph showing the effect of etching depth on the lateral pressure tuning of polymer coated FBG. The dotted lines represent the numerical simulation results.

6.4 ENHANCED THERMAL TUNING BY CLADDING ETCHING AND POLYMER COATING

Coating the CEFBG with a polymer having high thermal expansion coefficient can be utilized to enhance the temperature sensitivity. Theoretical formulation for the thermal tuning of coated FBG is carried out and is shown in Appendix B.

For experimental studies, the FBG is coated with silicone rubber having a thermal expansion coefficient of $520 \times 10^{-6} / ^\circ\text{C}$ and subjected to a controlled

temperature change in a heating chamber. The enhancement in the temperature sensitivity at various diameters is summarized in Figure 6.5. It was found from the experiments that the temperature sensitivity for bare FBG is 0.011 nm/°C where as it is 0.016, 0.023, 0.047 and 0.072 nm/°C for $D=2, 4, 7$ and 10 mm respectively. The wavelength shift is linear for all the diameters as seen from the graph.

The effect of cladding etching on the thermal tuning of a coated FBG with $D=4$ mm is shown in Figure 6.6. For the unetched FBG, the sensitivity is 0.023 nm/°C, while the etched FBGs with $d=102\text{ }\mu\text{m}$ and $d=78\text{ }\mu\text{m}$ gives 0.025 nm/°C and 0.032 nm/°C, respectively. Thus cladding etching can be effectively utilized to enhance the temperature sensitivity of polymer coated FBGs.

Figure 6.5 Effect of temperature on the wavelength shift sensitivity of a polymer coated FBG

Figure 6.6. Graph showing the effect of etching depth on the thermal tuning of polymer coated FBG

It was found that the adhesion between the fiber and the polymer coating plays an important during the thermal tuning process a detailed analysis of which is included in Appendix C. The contact between the fiber and the coating is modeled as a function of spring stiffness and a detailed analysis of its influence is included.

6.5 DESIGN OF ATHERMAL FBG

Despite the thermal effect on FBGs being utilized for various thermally tuned Bragg grating devices and temperature sensors, applications like non-temperature parametric sensors and optical filters require elimination of the temperature dependent wavelength fluctuations. A novel design for achieving the passive athermalisation, with improved design flexibility, compactness and simplicity, is presented in this section. The effective negative strain in the FBG upon a change in temperature is achieved by integrating cladding etching and polymer coating along with pretension in the fiber. The cladding

etching is utilized to reduce the axial load required for developing the compensating effect whereas the polymer coating is meant for enhancing the strain generated through thermal expansion.

6.5.1 Theoretical analysis

The schematic of the temperature-insensitive design is shown in Figure 6.7. After the gratings being written, the cladding of the FBG is etched to a diameter d . The fiber portion adjacent to the grating is coated with a polymer with a very high thermal expansion coefficient. Then both the ends of the fiber are fixed under pre-tension as shown in the Figure. When there is a temperature increase, the fiber along with the coating gets expanded and this leads to a release in the tension in the FBG. The release in the tension of a pre-strained member is equivalent to compressing the member. The release in the tension due to the thermal expansion of the polymer coating of a suitably designed structure would be exactly equal to the strain required for compensating the wavelength shift due to temperature increase. Thus, an increase in the temperature is associated with a release in the tension of the fiber equal in magnitude to a negative strain. Similarly a fall in the temperature is associated with an increase in the tension. The amount of compensation depends upon the depth of etching, type of polymer used for coating, the coating thickness and the coating length. The temperature dependence of the Bragg center wavelength λ_B can be summarized as [3]

$$\frac{\Delta\lambda_B}{\lambda_B} = (\zeta_n + \alpha_f)\Delta T \quad (6.1)$$

where $\Delta\lambda_B$ is the shift in center wavelength of an uncompensated FBG due to a temperature change of ΔT . ζ_n represents the thermo-optic coefficient of the fiber whose value is 8.6×10^{-6} for germanium doped silica core optical fiber and α_f represents the thermal expansion coefficient of the optical fiber.

Figure 6.7. Schematic of Athermal FBG

In the present setup, the wavelength shift due to thermo-optic effect is compensated by axial elongation. The strain dependence of the Bragg center wavelength can be formulated as the following equation, taking the values of photoelastic coefficients as $P_{11}=0.113$, $P_{12}=0.252$, refractive index $n_{co}=1.51$ and $\nu=0.16$ [31]:

$$\frac{\Delta\lambda_B}{\lambda_B} = 0.78\varepsilon_z \quad (6.2)$$

where ε_z is the axial strain in the FBG. With reference to Figure 6.7, the change in tension in the FBG, ΔF_1 , due to a temperature change ΔT can be written as,

$$\Delta F_1 = \alpha_f E_f A_f \left(\frac{L_1}{L_1 + L_2} \right) \Delta T \quad (6.3)$$

where α_f and E_f , respectively, represent the thermal expansion coefficient and the Young's modulus of the fiber. Similarly, the change in tension in the coated portion of the fiber ΔF_2 can be written as,

$$\Delta F_2 = (\alpha_c E_c A_c + \alpha_f E_f A_{f1}) \left(\frac{L_2}{L_1 + L_2} \right) \Delta T \quad (6.4)$$

where E_c and α_c , respectively, represent the Young's modulus and thermal expansion coefficients of the coating. A_f is the cross sectional area of the unetched fiber and A_{f1} represents the cross sectional area of FBG after etching.

$$A_f = \frac{\pi(2R)^2}{4} \quad (6.5a)$$

$$A_{f1} = \frac{\pi d^2}{4} \quad (6.5b)$$

The cross sectional area of the coating is indicated as A_c in the equation

$$A_c = \frac{\pi(2R + 2t)^2}{4} - \frac{\pi(2R)^2}{4} \quad (6.5c)$$

Here, $2R$ is the fiber diameter, d the diameter of FBG after etching and t , the coating

thickness. Therefore the effective change in the tension of the entire fiber is

$$\Delta F = \Delta F_1 + \Delta F_2 = \alpha_f E_f A_f \left(\frac{L_1}{L_1 + L_2} \right) \Delta T + (\alpha_c E_c A_c + \alpha_f E_f A_{f1}) \left(\frac{L_2}{L_1 + L_2} \right) \Delta T \quad (6.6)$$

In this equation, the first term represents the change in tension due to the thermal expansion/contraction of the FBG portion AB and the second term represents the same due to the thermal expansion/contraction of coated portion BC in Fig. 6.7. Therefore the axial compensating strain developed in the FBG due to the temperature change is

$$\varepsilon_z = \frac{\Delta F}{E_f A_{f1}} \quad (6.7)$$

From Equation 6.2, the compensating shift due to this axial strain could be calculated as,

$$\frac{\Delta\lambda_B}{\lambda} = 0.78 \frac{\Delta F}{E_f A_{f1}} \quad (6.8)$$

In order to make the FBG purely temperature independent, this compensating shift must be equal to the thermally induced wavelength shift as calculated from Equation 6.1.

Therefore,

$$0.78 \frac{\Delta F}{E_f A_{f1}} = (\zeta_n + \alpha_f) \Delta T \quad (6.9)$$

Substituting the value for ΔF from Eqn. 6.6,

$$0.78 \frac{\alpha_f E_f A_f \left(\frac{L_1}{L_1 + L_2} \right) \Delta T + (\alpha_c E_c A_c + \alpha_f E_f A_{f1}) \left(\frac{L_2}{L_1 + L_2} \right) \Delta T}{E_f A_{f1}} = (\zeta_n + \alpha_f) \Delta T \quad (6.9a)$$

Substituting the equation for A_f , A_{f1} and A_c from Eqn. 6.5,

$$0.78 \frac{\alpha_f E_f \frac{\pi(2R)^2}{4} \left(\frac{L_1}{L_1 + L_2} \right) \Delta T + \left(\alpha_c E_c \left(\frac{\pi(2R+2t)^2}{4} - \frac{\pi(2R)^2}{4} \right) + \alpha_f E_f \frac{\pi d^2}{4} \right) \left(\frac{L_2}{L_1 + L_2} \right) \Delta T}{E_f \frac{\pi d^2}{4}} = (\zeta_n + \alpha_f) \Delta T \quad (6.9b)$$

As seen from the above equations, the variables for achieving the athermalisation are coating thickness, the coating length and the depth of etching. By varying any of these parameters, the amount of compensation can be controlled.

Figure 6.8. Proposed configuration for packaging the device

The proposed schematic for packaging the device is shown in Figure 6.8. The cladding etched FBG is recoated and protected using a thin layer of polymer. The device can be enclosed in a cylindrical sleeve and both the ends of the fiber could be fixed to the sleeve as shown in the figure. In this case, the thermal expansion coefficient of the cylindrical sleeve also has to be taken into account.

6.5.2. Experiments

The schematic of the experimental setup is shown in Figure 6.9. Fiber Bragg gratings, with 10 mm length, are written on SMF-28 using the standard phase mask technique by frequency doubled argon ion laser at 244 nm. The fiber portion adjacent to the grating is then embedded co-axially in a cylindrical design of silicone rubber according to the schematic in Figure 6.7. The length of the coating used for the demonstration was 40 mm and the diameter is 10 mm. The FBG diameter is reduced through chemical etching for final adjustments of the wavelength shifts as close to zero as possible. The fiber

ends are fixed in pretension to a ceramic substrate as shown in Figure 6.9. An uncompensated FBG, written on the same fiber, is used in parallel for comparison. The device is kept inside a heating chamber to monitor the wavelength response with respect to temperature. The temperature of the heating chamber could be varied with an accuracy of 1°C. A broadband light source, and an optical spectrum analyzer (ANDO: AQ-6317B with resolution of 0.01 nm) were used for online analysis of the transmission spectrum.

Figure 6.9. Experimental set up for athermal FBG

The performance of the athermal FBG can be evaluated through the experimental results shown in Figures 6.10 and 6.11. Figure 6.10 shows the transmission spectrum at various temperatures. At a room temperature of 23 °C, the peak of the uncompensated FBG is centered at 1552.535 nm while that of athermal FBG is at 1554.665 nm. As the

temperature increases, it can be seen that the peak of the uncompensated FBG shifts towards the longer wavelength side while that of athermal FBG is centered at near the same location. Readings were taken at an interval of almost 10 °C till a maximum temperature of 102 °C. The wavelength shift with respect to temperature is plotted in Figure 6.11. For the reference FBG in which there is no temperature compensation, the total shift is 0.795 nm. For the athermal FBG, the maximum variation in the position of the peak is 0.025 nm in the temperature range of 23-102 °C.

Figure 6.10. Transmission spectrum at various temperatures. The numbers at the spectral notch represent the temperature in °C.

Figure 6.11. Wavelength shift with respect to temperature for a temperature compensated and uncompensated FBG

It is possible to control the axial strain in the FBG, during a temperature change, by varying the coating thickness and the coating length, which can also be noted from Equation 6.6. An increase in either the coating thickness or the length enhances the compensating effect. However, care should be taken to optimize the design for avoiding over compensation so that the peak center should not shift to the opposite side. Another important aspect, which has to be addressed, is the mechanical reliability of the FBG due to cladding etching. Even though the cladding diameter is reduced, the stress acting over the cross section of the fiber for generating a particular strain remains the same. Therefore the cladding etching does not affect the mechanical reliability or the fatigue life of the device.

The device provides a simple method for compensating the wavelength shift due to temperature changes of a FBG. The reduction of the diameter of the FBG by cladding etching reduces the force required for developing a particular strain for the

compensation process. This in turn leads to reduction of coating thickness of the polymer and makes the device more compact. Temperature compensation in this device is achievable for more than one combination of the design parameters; coating thickness, coating length and the depth of etching. This gives flexibility in design and selection of the parameters according to the specific requirements of the application.

6.6 SUMMARY

- Cladding etching combined with polymer coating is proposed and demonstrated as a technique to enhance the axial, lateral and thermal load sensitivity. The technique can be utilized for advanced sensing applications like multiparameter sensing, multi-axial strain sensing etc.
- A novel methodology has been proposed to model the contact condition between the polymeric coating and the silica fiber.
- A simple, compact and inexpensive method for achieving passive athermalisation of FBGs is proposed.

Chapter 7

MECHANICAL RELIABILITY STUDIES

7.1 INTRODUCTION

In this chapter, the mechanical reliability of the polymer packaged FBGs subjected to lateral loading is addressed. The fiber containing the grating and the attached pressure transmitting system are subjected to static and cyclic mechanical stresses for the purpose of wavelength tuning, which brings in the need for evaluating its reliability. In order to estimate the fatigue life, the values of maximum stress intensities obtained from FE analysis is compared with the standard stress-life data available in the literature. Since the failure criteria and mechanism is entirely different for each of the silica fiber, polymeric coating and metallic/polymeric contact media, the analysis has been done separately. The reliability of the FBG, the polymer coating and the contact media, during the process of wavelength tuning through transverse loading, is addressed respectively in sections 7.3 and 7.4. The theory of fatigue failure is briefly presented in section 7.2.

7.2 FRACTURE AND FATIGUE: THEORY

7.2.1 Fatigue of silica fiber containing gratings

Mechanical failure occurs when the applied stress exceeds the strength of the optical fiber. However, in the presence of defects, failure can occur much below the fracture strength. Surface imperfections, created at the fiber during the fiber fabrication process

or during mechanical stripping of the jacket for FBG fabrication, weaken the fiber. The failure strength of ceramic or glass is known to be inversely proportional to the largest or critical flaw present in the microstructure, as described by Griffith's law [114]. Based on this general law, one could calculate the fracture strength and predict the failure for a given flaw size. However, the microstructural flaws can grow over time due to the stress-induced corrosion mechanism that takes place at the crack tip and failure occurs even when the stresses applied to the component are below the component fracture strength. Number of loading cycles is yet another factor which could affect the time to failure besides the magnitude of the applied stress. However, it is already established by various studies [115-124] that silica fiber do not exhibit cyclic fatigue though failure still occurs by stress corrosion cracking. Therefore, for the purposes of making reliability estimates for optical fibers, the service stress can be modeled as a static stress.

The fracture of a silica fiber can be described by the subcritical crack growth model [114]

$$K_I = Y\sigma\sqrt{c} \quad (7.1)$$

where K_I is the stress intensity factor which is a convenient way of describing the stress distribution around a crack of length c , subjected to a stress σ . Y is the parameter representing the geometry of the crack. The power law relationship between the crack growth rate $\frac{dc}{dN}$ and the applied stress intensity range, ΔK , at the crack tip can be expressed as [114]

$$\frac{dc}{dN} = A(\Delta K)^n \quad (7.2)$$

where n can vary between 1 to 6 depending on the material and stress level and A is constant.

Assuming the applied stress σ_a to be static, the time to failure T_f is given by [125],

$$T_f = \frac{2}{AY^2(n-2)\sigma_a^n} \left(\frac{\sigma_i}{K_{IC}} \right)^{n-2} \quad (7.3)$$

where σ_i is the initial strength of the material in the absence of fatigue and related to initial crack length c_i by

$$K_{IC} = Y\sigma_i\sqrt{c_i} \quad (7.4)$$

7.2.2 Polymer fatigue

Unlike the behavior of ceramic materials, the fatigue of polymeric materials is highly dependent on the loading frequency [126,127]. Polymer fatigue behavior is also sensitive to temperature and environment. Depending on the stress amplitude and the frequency of load application, fatigue failure of polymers can occur by two means, thermal fatigue failure and mechanical fatigue failure. Thermal fatigue failure involves thermal softening (or yielding), which precedes crack propagation leading to ultimate failure. The high damping and low thermal conductivity of polymers cause a strong dependency of temperature rise on the rate of load application (frequency) and on the deformation level (stress or strain amplitude). This mechanism dominates in certain materials at large stress amplitudes within a particular range of frequency of load applications. At lower stress amplitude, on the other hand, a conventional form of fatigue crack propagation mechanism is generally observed [127]. Low frequency is

also found to cause fatigue fracture by conventional crack propagation at high stress amplitude [127]. An intermittent stress or a periodic stress usually constitutes a more severe condition than a continuous stress of the same magnitude. Thus in the case of polymers, fatigue crack initiation is dominated by time under load, while fatigue crack propagation is controlled by the number of cycles.

7.3 ANALYSIS OF FATIGUE OF FBG UNDER LATERAL COMPRESSION

In order to evaluate the fatigue life during lateral pressure tuning, the influencing maximum stresses are extracted from finite element analysis. The maximum value of alternating stress intensities in the fiber is calculated for an applied lateral load of 50 N acting over a length of 10 mm (5 N/mm) at various coating diameters and at various thicknesses of the contact media. (50 N is roughly 10 times as high as the normal working range for a tuning range of 1 nm). Analysis has also been done to find the influence of the stiffness of the coating material and the pressurizing media material on the fatigue life of the silica fiber.

7.3.1 Analysis of fatigue stresses in the fiber

For fatigue calculations, each loading (i) is compared to the next loading (j) to compute a maximum alternating stress at a particular location. The vector of stress differences between the loading steps i and j is computed as

$$\{\sigma\}_{ij} = \{\sigma\}_i - \{\sigma\}_j \quad (7.5)$$

where, $\{\sigma\}_i$ = stress vector for loading i and $\{\sigma\}_j$ = stress vector for loading j

The stress intensity ($\sigma_1(i,j)$) is computed based on $\{\sigma\}_{ij}$, using equation

$$\begin{vmatrix} \sigma_x - \sigma_0 & \sigma_{xy} & \sigma_{xz} \\ \sigma_{xy} & \sigma_y - \sigma_0 & \sigma_{yz} \\ \sigma_{xz} & \sigma_{yz} & \sigma_z - \sigma_0 \end{vmatrix} = 0 \quad (7.6)$$

The interim maximum alternating shear stress is,

$$\sigma_{ij}^d = \frac{\sigma_i(i, j)}{2} \quad (7.7)$$

Thus, at the selected node, the maximum of the stresses acting in all directions, σ_x , σ_y , σ_z , γ_{xy} , γ_{yz} , γ_{xz} (including shear) is calculated and utilized for fatigue life evaluation.

Figure 7.1 shows the maximum values of fatigue stresses (σ_x , σ_y , σ_z , γ_{xy} , γ_{yz} , γ_{xz}) in the fiber for $E=50$ MPa of the coating material. It could be seen that there is a slight decrease in the magnitude of all the stress values, except for σ_z that remains almost constant, with increase in coating diameter. σ_z dominates over the other tensile and shear stresses, which lies in the range of 10^7 N/m², by almost an order. The shear stress γ_{xy} , γ_{yz} and γ_{xz} represent the lowest of all.

Figure 7.1. Maximum values of fatigue stresses in the fiber for $E=50$ MPa of the coating

Figure 7.2. Maximum values of fatigue stresses in the fiber for $E=500$ MPa of the coating

Similar behavior can be observed for $E=500$ MPa of the coating material as can be seen from Figure 7.2. The stress in the axial direction, σ_z , is much higher than the stress components in the perpendicular and shear direction while all the stresses shows decreasing trend with increase in coating diameter. Figure 7.3 represents the case of a coating material with much higher stiffness. Unlike the previous cases, all principal stresses σ_x , σ_y and σ_z have almost the same magnitude while the shear stresses γ_{xy} , γ_{yz} and γ_{xz} differs significantly. However, it could be noted that the stresses in the principal directions σ_x , σ_y , σ_z , always dominates over the shear stresses γ_{xy} , γ_{yz} , γ_{xz} for all the cases of stiffness (E) values.

The effect of stiffness of the polymer coating on the fatigue stresses in the fiber can be evaluated with the help of Figure 7.4. The graph compares the maximum stress (σ_z) for different E values, as obtained from Figures 7.1, 7.2 and 7.3, at various coating

diameters. It could be seen that the lower the stiffness of the coating material, the higher will be the stress components. For example, for a coating diameter of 2000 μm , the stress value is only $2.38 \times 10^7 \text{ N/m}^2$ for $E=5000 \text{ MPa}$ whereas it is 1.35×10^8 and 2.92×10^8 respectively for $E=500 \text{ MPa}$ and 50 MPa of the coating material. Also it could be noted from the comparison of Figures 7.1, 7.2 and 7.3 that the difference between the magnitudes of principal and shear stresses increases with increase in the stiffness of the coating material.

The distribution of the principal and shear stresses could also be analyzed from the point of view of maximum tuning range. For improved tuning range σ_z should be high where as for better fatigue life, the stresses should remain as low as possible. Thus a compromise is to keep σ_z high and to reduce the chances of failure due to other stresses. This is achievable for a lower coating stiffness as can be seen from 7.1 where, σ_z remains much higher than σ_y , σ_x , γ_{xy} , γ_{yz} and γ_{xz} . Thus it can be ensured that the failure will occur only due to the axial stress σ_z .

Figure 7.3 Maximum values of fatigue stresses in the fiber for $E=5000 \text{ MPa}$ of the coating

Figure 7.4. Comparison of maximum values of fatigue stresses in the fiber for $E=50$, 500 and 5000 MPa of the coating

It would be interesting to analyze the effect of the properties of pressurizing media on the fatigue stresses of the uncoated optical fiber. The maximum values of fatigue stresses in the fiber for $E=5 \text{ MPa}$, 50 MPa , and 500 MPa of the contact plate is plotted in Figures 7.5, 7.6 and 7.7, respectively. It could be seen that the stress values are inconsistently dependent on the thickness t of the contact plate. The graphs are not following any regular trend with respect to the thickness t . Here, a noticeable difference from the previous case of FBGs with coatings is that the shear stresses γ_{xy} , γ_{yz} and γ_{xz} are significant and dominating over σ_x , σ_y and σ_z . Figure 7.8 shows the comparison of the maximum stresses for various E values. It can be seen that γ_{yz} corresponding to $E=5000 \text{ MPa}$ is much higher than shear stress values corresponding to $E=5 \text{ MPa}$ and 50 MPa for lower thicknesses of the contact plate. However, the difference becomes negligible for higher thickness values.

Figure 7.5. Maximum values of fatigue stresses in the fiber for $E=5$ MPa of the contact plate

Figure 7.6. Maximum values of fatigue stresses in the fiber for $E=50$ MPa of the contact plate

Figure 7.7. Maximum values of fatigue stresses in the fiber for $E=500$ MPa of the contact plate

Figure 7.8. Comparison of maximum values of fatigue stresses in the fiber for $E=5$, 50 and 500 MPa of the contact plate

7.3.2 Fatigue life evaluation

Figure 7.9 represents the predicted fatigue life data for silica fiber re-plotted from reference [115]. The horizontal axis represents the stress across the cross section (σ_z) of the fiber upon axial loading and the vertical represents the time to failure. The typical value of stress corrosion parameter n is around 20 for optical fiber and the stress intensity factor K_{IC} is $0.81 \text{ MPa.m}^{1/2}$ [114]

Figure 7.9. The fatigue life data for silica fiber [115]

Comparing with the analysis in section 7.3.1, it can be seen that the maximum value of stress intensities in the fiber for all the design configurations is very much less than the extrapolated fracture stress corresponding to the 25 years of life span. The critical value of stress for a life of 25 years is 0.7 GPa while the maximum value of calculated stress intensities for a designed load of 50 N (5 N/mm) lies well below this mark.

7.4 FATIGUE OF POLYMERIC COATING AND POLYMERIC PRESSURIZING MEDIA

In order to evaluate the fatigue performance of the polymeric coating and the pressurizing media, the maximum values of all the stresses σ_x , σ_y , σ_z , γ_{xy} , γ_{yz} and γ_{xz} , at various configurations is extracted similar to the previous case. The applied lateral load under consideration for all the analysis is 50 N which is almost 10 times the load required for a tuning range of 1 nm. The temperature is assumed constant and the effect of environment is not taken into account for the following analysis.

7.4.1 Analysis of stresses in the coating material

Figure 7.10 shows the maximum values of fatigue stresses in the coating material at various coating thicknesses for $E=50$ MPa of the coating. A clear trend of diminishing stress values can be observed with increase in the coating thickness. For lower coating thicknesses, the shear stress are dominating while for diameters above 3000 μm , σ_z becomes prominent. Similar behavior is repeated for $E=500$ MPa and $E=5000$ MPa as can be seen from Figures 7.11 and 7.12 respectively. σ_z is maximum in the diameter range 3000-4000 μm . Therefore, this diameter range can be recommended for suppressing all the other stresses without compromising on the wavelength shift sensitivity and the tuning range.

Figure 7.10 Maximum values of fatigue stresses in the coating material for $E=50$ MPa of the coating

Figure 7.11 Maximum values of fatigue stresses in the coating material for $E=500$ MPa of the coating

Figure 7.12 Maximum values of fatigue stresses in the coating material for $E=5000$ MPa of the coating

Figure 7.13 Graph showing the comparison of σ_z values in the coating material

7.4.2 Analysis of stresses in the pressuring media

The values of fatigue stresses in the contact plate for $E=5\text{MPa}$, 50 MPa and 500 MPa of the contact plate are shown respectively in Figures 7.14, 7.15 and 7.16, respectively. Its difficult to trace any consistent trends as in the previous case with respect to the coating diameter or with the stiffness values. The only generalized conclusion that can be drawn from the graphs is that the shear stresses γ_{xy} , γ_{yz} and γ_{xz} are dominating at lower thickness while σ_x , σ_y and σ_z become prominent at higher thicknesses. However, the data is useful for identifying the maximum intensities acting on the pressurizing media for fatigue life estimations.

Figure 7.14. Maximum values of fatigue stresses in the contact plate for $E=5\text{MPa}$ of the contact plate

Figure 7.15. Maximum values of fatigue stresses in the contact plate for $E=50 \text{ MPa}$ of the contact plate

Figure 7.16. Maximum values of fatigue stresses in the contact plate for $E=500 \text{ MPa}$ of the contact plate

7.4.3 Fatigue life evaluation

It can be seen from the available literature [126] that these values of fatigue stresses is very much less than critical value of stress which causes premature failure. As an example, the stress Vs cycles to failure for polymeric silicone rubber is re-plotted in Figure 7.17 from reference [126] for comparison with fatigue stresses obtained from finite element analysis. It can be seen that for a stress value above 220 MPa, the fatigue life is less than 5000 loading cycles where as it is 30,000 cycles if the stress value is 150 MPa. The fatigue life could be as high as 120, 000 cycles if the stress is less than a critical value of 130 MPa. The device is never expected to fail due to fatigue if the acting stress is less than 130 MPa.

From Figures 7.10 - 7.16, it can be seen that the maximum values of fatigue stresses in the silicone rubber coating and the compressing plate are approximately 7 MPa and 50 MPa respectively for a test load of 50 N. Thus, in comparison with the available fatigue data from the literature, the device is well under the critical fatigue limit for the operating ranges of stresses and loading cycles.

Figure 7. 17. Stress Vs cycles to failure for polymeric silicone rubber [126]

7.5 LIMITATIONS OF THE ANALYSIS

- Effect of temperature and humidity is not considered for the fatigue analysis (This has to be done especially for the polymeric material)
- Even though, due to time constraint in this project, the analysis is carried out using a well established and commonly used finite element software, it is recommended to experimentally verify the fatigue properties.
- The strength loss in the optical fiber due to the exposure to the UV radiation [128] is not taken into account.

7.6 SUMMARY AND CONCLUDING REMARKS

A comprehensive analysis of the reliability of the fiber and the pressure transmitting system subjected to lateral loading was carried out and presented in this chapter. The studies can be summarized as follows,

- For the optical fiber, the failure is independent on the number of loading cycles and depends only on the magnitude of the applied stress where as for the polymeric coating and contact plate, an intermittent stress or a periodic stress constitutes a more severe condition than a continuous stress of the same magnitude.
- The stresses at critical locations of the FBG for a tuning range of 1 nm are much below the fracture stress.
- Also, it can be noted that the fracture strength of the silica fiber is higher than that of the polymer coating.
- The polymer parts are more prone to failure than the optical fiber containing the grating. From this point of view also, cladding-etching process is not a threat to the reliability of the system as a whole.
- The life of the polymer coating and the polymeric contact media can be considered as the life of the sensor system.
- Reduction of the fiber diameter by cladding etching process does not affect the mechanical reliability of the optical fiber.

Chapter 8

EXPLORATION OF POTENTIAL APPLICATIONS

8.1 INTRODUCTION

In this chapter, a typical sensing application of the wavelength tuning through lateral loading is explored. The concept is utilized to design a device for measuring the handgrip strength which is an easy measure of skeletal muscle function as well as a powerful predictor of disability, morbidity and mortality. Experimental results show that the wavelength shifts of the order of 0.2-0.5 nm could be achieved for individual fingers. The device is calibrated in terms of load to convert the wavelength shift to the strength of the grip. The time dependent wavelength fluctuations is also studied and presented in this chapter.

8.2 BACKGROUND

Grip strength has extensively being used as a measure to monitor different physical and functional disorders related to healthcare [129]. It is rated as one among the top 10 fitness tests [130]. It is used as a tool to evaluate individual's physical work capacity to perform physically demanding work tasks [131]. The performance of hand and arm function has also been used to identify various physiological disabilities. Grip strength measurement is included in many rehabilitation programs to monitor the patient's progress.

The reduction of grip strength is often associated with numerous functional disorders of internal organs as well as muscular and neurological dysfunctions, osteoporosis and related bone disorders. Various studies have shown that a steeper decline in grip strength is associated with ageing, weight loss and chronic conditions such as stroke, diabetes, arthritis, coronary heart disease, and chronic obstructive pulmonary disease. Many factors such as physiological conditions are associated with the grip strength and thereby studied in terms of grip strength. It was reported that poor hand strength as measured by is a predictor of disability in older people [132]. The handgrip test is an easy and inexpensive tool to identify elderly people at the risk of disability. Handgrip strength is reported as a suitable phenotype for identifying genetic variants of importance to mid- and late-life physical functioning [133]. Handgrip strength is known to be associated with muscular functioning in other muscle groups and with activities of daily living functioning, and it predicts incident disability. Another interesting study of grip strength has found that men with a strong grip are more likely to develop osteoarthritis in certain joints of their hands and fingers [134]. Hand osteoarthritis contributes to hand dysfunction, mainly related to the severity of osteoarthritis, pain, joint involvement and the presence of nodules [135]

It was reported by American geriatric society that among older disabled women, handgrip strength was a powerful predictor of cause-specific and total mortality and one that may help identify patients at increased risk of deterioration of health [136]. The association of rate of strength decline with weight change and chronic conditions [137] has also been reported. Another study was reported regarding the grip and pinch strength differences between sides for the right and left-handed population [138]. A statistically significant difference was found between the grip and pinch strengths of dominant and nondominant hands in favor of the dominant hand. The dominant hand is

significantly stronger in right handed subjects but no such significant difference between sides could be documented for left handed people.

A proper and standard testing procedure is of great importance in determining the effectiveness of grip testing method. In addition, normative data are needed to interpret evaluation data, to set realistic treatment goals, and to assess a patient's physiological conditions. [139,140]. Various factors, which influence the testing process, are following, Grip span and grip type [141], hand positions, loading type (static, dynamic etc.) [142], wrist position [143], arm positions [144,145], hand torque [146], handle shape design and attributes [147,148]. Standards for grip testing have already been established.

The current Grip testing methods include the use of a hydraulic [149] or electronic dynamometer [150]. These mechanical instruments are too rugged and do not offer comfortable measurements, especially in rehabilitation programs. Moreover, these instruments cannot provide individual finger strengths apart from measuring the hand strength. Therefore a simple, low cost optical technique to measure the individual finger strengths is proposed utilizing the concept of LPT of FBGs.

8.3 DESIGN AND CALIBRATION OF THE GRIP SENSOR

8.3.1 Device design

The length of each FBG is 10 mm. According to the schematic shown in Figure 8.1, five gratings were written at different Bragg wavelengths in a single line for representing 5 fingers. The sixth FBG is used as reference FBG and for temperature independent measurements.

Figure 8.1. Schematic of gratings fabricated on photosensitive fiber for individual fingers

The schematic of the grip holder is as shown in Figure 8.2. Grooves are made in a cylindrical grip holder to suit the fingers and the fiber containing the grating is sandwiched between rubber bushes as shown in the schematic. The pressure applied by the fingers onto the rubber bush transfers to the fiber and strains the FBG. And the wavelength shift produced by straining the FBG is proportional to the strength of the grip.

8.3.2 Experimental setup

The experimental set up is shown in Figure 8.3. A broadband tunable light source is connected to the fiber lead through a 2×2 coupler as shown in Figure 8.3, and the reflection spectrum is observed using an optical spectrum analyzer (ANDO: AQ-6317B with resolution of 0.01 nm).

Figure 8.2. Schematic of the grip holder

Figure 8.3. Experimental setup

8.3.3 Device Calibration

Calibration of the device is done by subjecting the FBG to a test load under similar conditions. The FBG, sandwiched between two rubber sheets of same configuration as used in the grip holder, is compressed between two ceramic plates under a test load as shown in Figure 8.4. The load is applied from the top and the reflected spectrum is observed and recorded online. The experiments are done at a constant room temperature.

Figure 8.5 shows the reflection spectrum at various test loads. The peak of the reference FBG, which is not subjected to any load, remains at 1543.43 nm indicating constant room temperature during the test process. At zero load the peak of the test FBG is centered at 1539.872 nm, which shifts to 1540.08 nm upon an applied load of 51.94 N. Test has been done up to a maximum load of 296.9 N, as the normal grip

strength for human beings falls below that value [139]. It has to be noted that only half of the applied load is acting on the test FBG. The linear fit for the calibration curve is shown in Figure 8.6. The wavelength shift sensitivity to the applied lateral is calculated as 0.0052 nm/N. Repeated loading and unloading has been done 10 times. The maximum error is 0.024 nm and the corresponding measurement error in load is 4.61 N. The wavelength response upon a test case of loading and unloading is shown in Figure 8.7. It can be seen that the curves are maintaining linearity and following almost the same path. From the curve, it can be concluded that the device can be used up to a maximum load of 150 N in the linear range for a finger. Repeatability of the measurements is evaluated for a test load of 26 N and the results are shown graphically in Figure 8.8. Repeated loading is done 10 times and it has been found that the maximum deviation from linearity amounts to 0.024 nm.

Figure 8.4. Experimental setup for calibration

In order to evaluate the thermal stability, the device is tested at 4 different temperatures, room temperature 21 °C, 25 °C, 30 °C and 35 °C. It was found that the peaks of the test FBG and reference FBG shift at the same rate of 0.0112 nm/°C. However the temperature dependence of the reflected wavelength will not affect the test results since

the separations between the reference FBG and test FBGs always remain the same independent of temperature.

Figure 8.5. Reflection spectrum at various test loads

Figure 8.6. Calibration curve

Figure 8.7. Graph showing the wavelength shifts upon loading and unloading

Figure 8.8. Repeatability for a test load of 26 N

Since the FBG is pressed between rubber bushes, it is worth studying the effect of viscoelasticity on the stability of the readings. The temporal response of the device is studied under various loads and shown graphically in Figure 8.9. The wavelength shift is plotted as a function of time. The wavelength rises abruptly upon loading, which gradually decays and comes to constant level within a few seconds. This is due to the

viscoelastic nature of the rubber. For example, for a test load of 51.94 N, the wavelength shifts to 1540.24 nm upon loading but drops back to 1540.008 nm after 5 seconds and remains constant. Similar behavior is observed during the unloading process as shown in Figure 8.9b. Upon removal of a load, the wavelength drops to a lower value but immediately stabilizes within a few seconds. Therefore, in order to get reliable measurements, readings have to be taken at least 5 seconds after the application of the load.

Figure 8.9. Temporal response under various loads (a) loading (b) Unloading

8.4 DISCUSSION OF A TEST CASE

The working of the sensor can be explained with the help of Figure 8.10, which shows the reflection spectrum obtained from OSA. The firm line represents the undisturbed spectrum while the dotted line indicates the change in the reflection spectrum upon application of the gripping pressure. It can be seen that there is no shift in the center wavelength of the reference FBG, which was centered at 1539.34 nm, while all the other peaks shift with discrete amounts. The shifts of the center wavelength are

calibrated in terms of pressure, and it can be used to quantify the grip strength of individual fingers. For example, the peak of FBG 2, which represents the little finger, was centered initially at 1542.94 nm got shifted to 1543.18 nm upon application of the pressure. Therefore the value of absolute shift is 0.24 nm. In order to make the readings temperature independent, the shifts can be calculated with respect to the reference FBG. Table 8.1 shows absolute shifts and the shifts with respect to the reference FBG for all the five peaks.

Table 8.1

	Peak center (nm)		Absolute shift (nm)	Shift w.r.t reference FBG (nm)
	Before	After		
FBG 1 (Reference)	1539.36	1539.36	0	0
FBG 2	1542.94	1543.2	0.26	0.26
FBG 3	1544.76	1545.08	0.32	0.32
FBG 4	1546.28	1546.54	0.26	0.26
FBG 5	1547.84	1548.08	0.24	0.24
FBG 6	1549.32	1549.60	0.28	0.28

Figure 8.10. Reflection spectrum from the grip sensor

Figure 8.11. Plot of grip strength for the test case

Figure 8.11 shows the plot of grip strength of individual fingers. In addition to the advantage of measuring individual finger strengths, the total grip strength can be calculated as the sum of strengths of individual fingers. In this case, the calculated handgrip strength is 256.5 N.

8.5 SUMMARY

A novel fiber optic approach to measure grip strength was proposed and demonstrated. Strain dependent wavelength response of FBGs is utilized to obtain the strength of individual fingers. Five FBGs, written at different center wavelengths on a single photosensitive fiber, were used to get the response from individual finger. The fiber containing the gratings was attached to a suitable grip holder, which can effectively transfer the grip force to the FBGs. The device is calibrated in terms of load to convert the shift in the center wavelength to the grip strength. The device can be used up to a maximum load of 150 N in the linear range. The device was tested under different temperatures to study the temperature dependence. The temperature sensitivity of the

reflected wavelength is found to be $0.0112 \text{ nm/}^{\circ}\text{C}$. An additional reference FBG was incorporated to provide temperature independent measurement. The temporal response of the device is also studied under various loads. The wavelength rises abruptly upon loading, which gradually decays and comes to constant level within a few seconds. The device is tested and the preliminary experimental results are in good corroboration with the reported values of grip strengths of a healthy human being.

CHAPTER 9

CONCLUSIONS AND RECOMMENDATIONS FOR FUTURE WORK

Wavelength tuning through lateral loading remained unexplored for sensing applications for two decades after the discovery of photosensitivity in optical fiber and the followed development of fiber Bragg gratings. Even though this was due to its inherent limitations, the positive aspects of LPT were neglected in this way. However a good effort has been taken to investigate the problem in depth and to address the shortcomings in the current research. The conclusions drawn out of the research and the directions for future works are summarized in this chapter.

9.1 CONCLUSIONS

The pressurizing media, which is indispensable in the lateral pressure tuning process, plays an important role in dictating the wavelength response of the FBG. A comprehensive analysis of this influence was carried out and the results are positive from the point of exploiting it for enhanced performance.

It was found that a large contact width ($>100\text{ }\mu\text{m}$ for $125\text{ }\mu\text{m}$ fiber) between pressurizing media and the fiber, which is a function of the contact stiffness, would be helpful in effective transfer of the applied lateral load to the FBG. This also makes the load transfer more hydrostatic in nature and thereby enhances the lateral pressure sensitivity and the wavelength tuning range. Thus the stiffness of the contact media is

important and one with low stiffness is highly favourable for higher sensitivity and reduced birefringence. Also, the wavelength shift sensitivity is highly dependent on the thickness of the pressuring media and an optimum value, which depends on the contact material and the contact conditions, has to be selected for enhancing the sensitivity. Despite the increase in sensitivity with thickness, optimal coating thickness is to be met as birefringence increases with thickness. Efforts have to be taken for improving the contact friction while designing the pressure transmitting system for the lateral pressure tuning since a higher contact friction enhances wavelength shift sensitivity. However it could be noted that the sensitivity enhancement effect at higher thickness and high contact friction is more predominant for contact media with low stiffness values.

Theoretical and experimental studies on LPT of the proposed polymer coated FBG showed that the technique could be adopted to overcome the existing limitations of wavelength tuning through lateral loading. The technique is promising since significant increase in the sensitivity could be achieved and the birefringence could be reduced to an insignificant near zero value. Even though polymers with low Young's modulus values are preferred for enhanced sensitivity, the final selection also depends on the influence of the application environment. The fact that the birefringence depends little on the coating material, but much on the coating diameter, allows flexibility in the selection of the materials properties. Both from this point of view and from the perspective of maximizing the sensitivity, selecting the optimized coating diameter is very important, since there exists a critical value above which the sensitivity decreases.

The studies also showed that an optimally designed polymer coating could be adopted to enhance the hydrostatic pressure sensitivity. In the case of mechanically induced hydrostatic pressure or in the case of hydrostatic pressure exerted by a flexible

fluid media, where there is no physical restriction on the axial movement, a coating material with low stiffness value is highly favourable for enhanced sensitivities.

Cladding etching of the FBG combined with polymer coating of the FBG could be utilized as an effective method for further improvement in the lateral pressure sensitivity. The technique could also be used for enhancing the axial load and temperature sensitivities along with significant lateral pressure sensitivity, which is highly useful for multiparameter sensing and for advanced sensing applications like multi-axial force sensing. The simple, inexpensive method provides a single compact design which simultaneously enhances the sensitivity of all the major parameters without compromising the mechanical reliability. Moreover, the principle could be adopted for eliminating the temperature dependence wherever it is required.

The theoretical investigation into the mechanical reliability of the FBG and the attached system provides valuable information for the device design and for assessing the life of the component. For the optical fiber, the failure is independent on the number of loading cycles and depends only on the magnitude of the applied stress where as for the polymeric coating and contact plate, an intermittent stress or a periodic stress constitutes a more severe condition than a continuous stress of the same magnitude. Also it was found that the governing stresses in both the fiber and the coating/contact media are much below the critical limits and therefore the failure is more influenced by the loading cycles. Thus the polymer parts are more prone to failure than the optical fiber containing the grating and therefore the life of the polymer coating and the polymeric contact media can be considered as the life of the sensor system.

9.2 INNOVATIONS

1. A pioneer effort has been taken to address the shortcomings and to enhance the performance of FBGs subjected to lateral pressure tuning
2. The influence of contact media on the wavelength response characteristics of FBGs subjected to lateral pressure tuning is investigated for the first time
3. A simple, effective and inexpensive methodology, namely the polymer coating technique, has been proposed to address the issues of low lateral pressure sensitivity and birefringence
4. A simple and compact design, namely polymer coated cladding etched FBG, is proposed for the first time to enhance the lateral pressure sensitivity along with axial load and temperature sensitivities
5. The mechanical reliability of FBGs subjected to lateral pressure tuning is investigated for the first time
6. A novel fiber optic sensor, based on the concept of LPT of FBGs, for hand grip strength monitoring has been devised

9.3 RECOMMENDATIONS FOR FUTURE WORK

9.3.1 Effect of polymer coating on LPT of special gratings

Isolated efforts have already been taken to utilize the advantages offered by special gratings [94, 97, 98]. Therefore the influence of the contact media and the effect of polymer coating on the LPT of special gratings are worth studying.

Polymer coating technique can be adopted to enhance the performance of a pi-phase shifted FBG, in which its birefringence can be utilized for accurate transverse load measurements. The polymer coating and the contact media can be optimized from the point of increasing the stress induced birefringence. The enhancement in the birefringence can be utilized for increasing its sensitivity.

It was proposed and demonstrated [97] that long period gratings (LPG) are several hundred times more sensitive to transverse load and its polarization mode splitting due to birefringence can be utilized for transverse load measurements. In this case, the polymeric coating can be utilized to eliminate the birefringence completely and thereby the wavelength detection scheme can be based only on the shift of the single transmission peak and not on the measurement of the shift of the orthogonal polarized modes. Moreover, the polymer coating can also be optimized to reduce the high temperature dependence of the LPG.

9.3.2 Utilization of LPT of polymer coated FBGs for spectral shaping applications

It was reported that spectral characterization and shaping could be achieved by applying a transverse force to a small grating section. A typical dip of transmission appears in the spectral response of the grating and the chirp profile of a chirped FBG is possible by pressing the grating in different positions [93]. The spectral shaping by local pressing attracts applications like fiber polarizer and add drop filter for WDM.

Therefore the effect of polymeric coating on the localized pressing for spectral shaping applications is worth evaluating. The dependence of the coating material and the coating thickness and the coating profile could be characterized for the ease of

spectral shaping. Based on this, the potentials of localized pressing and spectral shaping can be explored further.

9.3.3 Exploration of potential applications

The advantages offered by the LPT of FBGs can be harnessed for a variety of sensing applications. The flexibility and ease in mounting the FBG for the purpose of lateral loading makes it suitable for applications like vibration measurements and dynamic strain measurements. Similarly, the robustness of the polymer coated FBG for LPT makes it a potential candidate for impact load measurements.

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APPENDIX A

A.1 Schematic of FBG

subjected to diametric concentrated load

Figure A.1 Schematic of FBG subjected to diametric concentrated load

The stress distribution upon the application of a diametrical concentrated load as shown in figure A.1 can be expressed as [113],

$$\sigma_x(x, y) = \frac{-2P}{\pi L} \left\{ \frac{x^2(R+y)}{[x^2 + (R+y)^2]^2} + \frac{x^2(R-y)}{[x^2 + (R-y)^2]^2} - \frac{1}{2R} \right\} \quad (\text{A.1})$$

$$\sigma_y(x, y) = \frac{-2P}{\pi L} \left\{ \frac{(R+y)^3}{[x^2 + (R+y)^2]^2} + \frac{(R-y)^3}{[x^2 + (R-y)^2]^2} - \frac{1}{2R} \right\} \quad (\text{A.2})$$

Here, the effect of contact area and the influence of the contact media are not taken into account. The applied load is considered as a point load and therefore the contact length is assumed to be zero. Equations A.1 and A.2 can be simplified as,

$$\sigma_x(x, y) = \frac{-2P}{\pi L} \left\{ \frac{x^2(R+y)}{[x^2 + y^2 + R^2 + 2Ry]^2} + \frac{x^2(R-y)}{[x^2 + y^2 + R^2 - 2Ry]^2} - \frac{1}{2R} \right\} \quad (\text{A.3})$$

$$\sigma_y(x, y) = \frac{-2P}{\pi L} \left\{ \frac{(R+y)^3}{[x^2 + y^2 + R^2 + 2Ry]^2} + \frac{(R-y)^3}{[x^2 + y^2 + R^2 - 2Ry]^2} - \frac{1}{2R} \right\} \quad (\text{A.4})$$

For converting to polar co ordinates, substitute,

$$r = (x^2 + y^2)^{\frac{1}{2}} \quad (\text{A.5})$$

$$x = r \cos \theta \quad (\text{A.6})$$

$$y = r \sin \theta \quad (\text{A.7})$$

Therefore Equation A.3 & A.4 becomes,

$$\sigma_x(r, \theta) = \frac{-2P}{\pi L} \left\{ \frac{r^2 \cos^2 \theta (R + r \sin \theta)}{[r^2 + R^2 + 2Rr \sin \theta]^2} + \frac{r^2 \cos^2 \theta (R - r \sin \theta)}{[r^2 + R^2 - 2Rr \sin \theta]^2} - \frac{1}{2R} \right\} \quad (\text{A.8})$$

$$\sigma_y(r, \theta) = \frac{-2P}{\pi L} \left\{ \frac{(R + r \sin \theta)^3}{[r^2 + R^2 + 2Rr \sin \theta]^2} + \frac{(R - r \sin \theta)^3}{[r^2 + R^2 - 2Rr \sin \theta]^2} - \frac{1}{2R} \right\} \quad (\text{A.9})$$

Assumption

Under the assumption

In the presence of a deformable contact media, the contact pressure varies along the circumference as a function of θ . The pressure distribution $p(\theta_l)$ along the contact surface for a load P can be expressed as

$$p(\theta_l) = \frac{2P}{\pi L} \frac{\cos \theta_l}{R} \quad (\text{A.10})$$

It could be noticed that the magnitude of the pressure depends on the radius of the fiber R and also on the length of the FBG.

Figure A.2 Schematic of the geometry of FBG subjected to a distributed lateral load

In the contact area, consider an elemental strip at an angle θ_l from the Y-axis subtending an angle $d\theta_l$ at the center. The surface area of this elemental strip is $LRd\theta_l$.

The total load dP acting on this element can be expressed as,

$$dP = p(\theta_l)LRd\theta_l \quad (\text{A.11})$$

Substituting the expression for pressure distribution $p(\theta_l)$ from Equation A.10, we get

$$\therefore dP = \frac{2P}{\pi} \cos \theta_l d\theta_l \quad (\text{A.12})$$

The incremental stresses at any point (r, θ) due to this elemental force dP can be obtained by substituting Equation A.12 in Equations A.8 and A.9 and by replacing θ as $(\theta - \theta_l)$.

APPENDIX A

$$d\sigma_x(r, \theta) = \frac{-2\left(\frac{2P}{\pi} \cos\theta_1 d\theta_1\right)}{\pi L} \left\{ \frac{r^2 \cos^2(\theta - \theta_1)(R + r \sin(\theta - \theta_1))}{[r^2 + R^2 + 2Rr \sin(\theta - \theta_1)]^2} + \frac{r^2 \cos^2(\theta - \theta_1)(R - r \sin(\theta - \theta_1))}{[r^2 + R^2 - 2Rr \sin(\theta - \theta_1)]^2} - \frac{1}{2R} \right\} \quad (\text{A.13})$$

$$d\sigma_y(r, \theta) = \frac{-2\left(\frac{2P}{\pi} \cos\theta_1 d\theta_1\right)}{\pi L} \left\{ \frac{(R + r \sin(\theta - \theta_1))^3}{[r^2 + R^2 + 2Rr \sin(\theta - \theta_1)]^2} + \frac{(R - r \sin(\theta - \theta_1))^3}{[r^2 + R^2 - 2Rr \sin(\theta - \theta_1)]^2} - \frac{1}{2R} \right\} \quad (\text{A.14})$$

Therefore the total stresses can be found out by integrating Equations A.13 and A.14 over the contact length $2a$.

$$\sigma_x(r, \theta) = \int_{-\phi}^{+\phi} \frac{-2\left(\frac{2P}{\pi} \cos\theta_1 d\theta_1\right)}{\pi L} \left\{ \frac{r^2 \cos^2(\theta - \theta_1)(R + r \sin(\theta - \theta_1))}{[r^2 + R^2 + 2Rr \sin(\theta - \theta_1)]^2} + \frac{r^2 \cos^2(\theta - \theta_1)(R - r \sin(\theta - \theta_1))}{[r^2 + R^2 - 2Rr \sin(\theta - \theta_1)]^2} - \frac{1}{2R} \right\} \quad (\text{A.15})$$

$$\sigma_y(r, \theta) = \int_{-\phi}^{+\phi} \frac{-2\left(\frac{2P}{\pi} \cos\theta_1 d\theta_1\right)}{\pi L} \left\{ \frac{(R + r \sin(\theta - \theta_1))^3}{[r^2 + R^2 + 2Rr \sin(\theta - \theta_1)]^2} + \frac{(R - r \sin(\theta - \theta_1))^3}{[r^2 + R^2 - 2Rr \sin(\theta - \theta_1)]^2} - \frac{1}{2R} \right\} \quad (\text{A.16})$$

where 2ϕ is the contact angle which can be expressed as,

$$\phi = \frac{a}{R} \quad (\text{A.17})$$

and the contact length

$$2a = 4 \left[\frac{PR}{\pi L} \frac{(1 - \nu_2^2)E_1 + (1 - \nu_1^2)E_2}{E_1 E_2} \right]^{1/2} \quad (\text{A.18})$$

Appendix B

THEORETICAL FORMULATION FOR THERMAL TUNING OF COATED FBG BASED ON COUPLED MODE THEORY

Theoretical model for thermal tuning of bare FBG is developed and it is modified for thermal tuning of polymer coated FBGs. Theoretical formulations are done based on the standard coupled mode equations for guided wave optics. The model could be used for simulating the performance of the device at various conditions.

Let L be the length of the grating, Λ the period and $\overline{\delta n_{eff}}$ be the modulation in the effective refractive index of the fiber core. Equations 2.33 and 2.34 represent the expression for amplitude and power reflection coefficients ρ and r of a fiber Bragg grating based on coupled mode theory.

To find the effect of temperature on the reflection spectra of a Bragg grating, equation 2.34 can be differentiated with respect to temperature. Let us take the variable, $y = \sqrt{k^2 - \hat{\sigma}^2}$, for ease of mathematical calculations. Equation 2.34 becomes,

$$r = \frac{\sinh^2(yL)}{\cosh^2(yL) - \frac{\hat{\sigma}^2}{k^2}} \quad (\text{B.1})$$

Then the temperature dependence of y can be summarized as,

$$\frac{dy}{dT} = \frac{k \frac{dk}{dT} - \hat{\sigma} \frac{d\hat{\sigma}}{dT}}{\sqrt{k^2 - \hat{\sigma}^2}} \quad (\text{B.2})$$

Differentiating equation B.1 with respect to temperature T ,

$$\frac{dr}{dT} = \frac{\left[2 \sinh(yL) \cos(yL) \left(\cosh^2(yL) - \frac{\hat{\sigma}^2}{k^2} \right) \left(y \frac{dL}{dT} + L \frac{dy}{dT} \right) \right]}{\left(\cosh^2(yL) - \frac{\hat{\sigma}^2}{k^2} \right)^2} - \frac{\left[\sinh^2(yL) \left[2 \sinh(yL) \cos(yL) \left(y \frac{dL}{dT} + L \frac{dy}{dT} \right) - \left(2 \hat{\sigma} \frac{d\hat{\sigma}}{dT} - \frac{2\sigma^2}{k^3} \frac{dk}{dT} \right) \right] \right]}{\left(\cosh^2(yL) - \frac{\hat{\sigma}^2}{k^2} \right)^2} \quad (\text{B.3})$$

Again, Equation B.3 can be expanded incorporating the values of $\frac{dk}{dT}$ and $\frac{d\hat{\sigma}}{dT}$.

From Equation 2.32, $k = \frac{\pi}{\lambda} \nu \bar{\delta n}_{eff}$

$$\therefore \frac{dk}{dT} = \frac{\pi}{\lambda} \nu \frac{d\bar{\delta n}_{eff}}{dT} \quad (\text{B.4})$$

But the modulation of refractive index $\bar{\delta n}_{eff}$ along the core of the fiber is very small and

of the order of 10^{-3} to 10^{-5} . $\therefore \frac{d\bar{\delta n}_{eff}}{dT} \rightarrow 0$. And hence the temperature dependence of

$\bar{\delta n}_{eff}$ can be neglected.

$$\therefore \frac{dk}{dT} \rightarrow 0 \quad (\text{B.5})$$

$$\text{Similarly, } \frac{d\sigma}{dT} = \frac{2\pi}{\lambda} \frac{d\bar{\delta n}_{eff}}{dT} \rightarrow 0 \quad (\text{B.6})$$

And for uniform Bragg gratings, chirp along z-axis $\frac{d\phi}{dz} = 0$

Therefore from Equation 2.30,

$$\frac{d\hat{\sigma}}{dT} = \frac{d\delta}{dT} + \frac{d\sigma}{dT} - \frac{1}{2} \frac{d^2\phi}{dz \cdot dT} \Rightarrow \frac{d\delta}{dT} \quad (\text{B.7})$$

where $\delta = 2\pi n_{eff} \left(\frac{1}{\lambda} - \frac{1}{\lambda_B} \right)$ from Equation 2.31

The temperature gradient dT affects the detuning δ as follows.

$$\frac{d\delta}{dT} = \frac{2\pi}{\lambda} \frac{dn_{eff}}{dT} + \frac{\pi}{\Lambda^2} \frac{d\Lambda}{dT} \quad (B.8)$$

$$\therefore \frac{d\hat{\sigma}}{dT} = \frac{2\pi}{\lambda} \frac{dn_{eff}}{dT} + \frac{\pi}{\Lambda^2} \frac{d\Lambda}{dT} \quad (B.9)$$

In the above equation, the term $\frac{dn_{eff}}{dT}$ represents the change in the effective refractive index of the core of the fiber with respect to temperature. To evaluate this, we can introduce the term thermo-optic coefficient ζ_n , which is defined as [31],

$$\zeta_n = \frac{1}{n_{eff}} \frac{dn_{eff}}{dT} \quad (B.10)$$

$$\therefore \frac{dn_{eff}}{dT} = \zeta_n n_{eff} \quad (B.11)$$

Similarly, $\frac{d\Lambda}{dT}$ represents the variation in the period of the grating with change in temperature. We can relate this to the coefficient of thermal expansion α_{co} of the fiber core as follows [31]

$$\alpha_{co} = \frac{1}{\Lambda} \frac{d\Lambda}{dT} \quad (B.12)$$

$$\therefore \frac{d\Lambda}{dT} = \alpha_{co} \Lambda \quad (B.13)$$

Also, $\frac{dL}{dT}$ represents the change in the length of the portion of fiber containing grating.

So it can be written as,

$$\frac{dL}{dT} = \alpha_{co} L \quad (B.14)$$

If we substitute equation (B.4) to (B.14) in equation (B.2) and (B.3) and solve these equations simultaneously, we can predict the temperature dependence of r .

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$$\frac{dr}{dT} = \left[\frac{2 \sinh(\sqrt{k^2 - \hat{\sigma}^2} L) \cos(\sqrt{k^2 - \hat{\sigma}^2} L) \left(\cosh^2(\sqrt{k^2 - \hat{\sigma}^2} L) - \frac{\hat{\sigma}^2}{k^2} \right)}{\left(\cosh^2(\sqrt{k^2 - \hat{\sigma}^2} L) - \frac{\hat{\sigma}^2}{k^2} \right)^2} \right] \times \left[\left(\sqrt{k^2 - \hat{\sigma}^2} \alpha_{co} L - L \left(\frac{\hat{\sigma}}{\sqrt{k^2 - \hat{\sigma}^2}} \left(\frac{2\pi \zeta_n n_{eff}}{\lambda} + \frac{\pi \alpha_{co}}{\Lambda} \right) \right) \right) \right] -$$

$$\left[\frac{2 \sinh^3(\sqrt{k^2 - \hat{\sigma}^2} L) \cosh(\sqrt{k^2 - \hat{\sigma}^2} L) \left(\sqrt{k^2 - \hat{\sigma}^2} \alpha_{co} L - L \left(\frac{\hat{\sigma}}{\sqrt{k^2 - \hat{\sigma}^2}} \left(\frac{2\pi \zeta_n n_{eff}}{\lambda} + \frac{\pi \alpha_{co}}{\Lambda} \right) \right) \right)}{\left(\cosh^2(\sqrt{k^2 - \hat{\sigma}^2} L) - \frac{\hat{\sigma}^2}{k^2} \right)^2} \right] - \left[\frac{2 \hat{\sigma} \sinh^2(\sqrt{k^2 - \hat{\sigma}^2} L) \left(\frac{2\pi \zeta_n n_{eff}}{\lambda} + \frac{\pi \alpha_{co}}{\Lambda} \right)}{\left(\cosh^2(\sqrt{k^2 - \hat{\sigma}^2} L) - \frac{\hat{\sigma}^2}{k^2} \right)^2} \right]$$

(B.15)

Equation B.15 represents the complete model for the temperature dependence of the reflective spectra of a uniform fiber Bragg grating.

Now consider the case of a fiber Bragg grating etched to a diameter d and coated with a polymer to a diameter D . Considering the general equation for thermal strain due to a temperature difference ΔT , the respective strains in the core, cladding and coating can be written as [112]

$$\varepsilon_{zco} = \frac{\sigma_{zco}}{E_f} + \alpha_{co}(\Delta T) \quad (B.16)$$

$$\varepsilon_{zcl} = \frac{\sigma_{zcl}}{E_f} + \alpha_{cl}(\Delta T) \quad (B.17)$$

$$\varepsilon_{zc} = \frac{\sigma_{zc}}{E_c} + \alpha_c(\Delta T) \quad (B.18)$$

α_{co}, α_{cl} and α_c is the thermal expansion coefficients of the core, cladding, and the coating layer respectively. E_f and E_c represents the modulus of elasticity of fiber and the coating. $\sigma_{zco}, \sigma_{zcl}$ and σ_c represents the axial stresses in the core, cladding, and coating due to the temperature difference ΔT .

Let us assume that ε_{eff} is the effective strain in the axial direction due the combined effect of etched cladding and the coating.

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$$\therefore \varepsilon_{eff} = \varepsilon_{zco} = \varepsilon_{zcl} = \varepsilon_{zc} \quad (B.19)$$

$$\varepsilon_{eff} = \frac{\sigma_{zco}}{E_f} + \alpha_{co}(\Delta T) = \frac{\sigma_{zcl}}{E_f} + \alpha_{cl}(\Delta T) = \frac{\sigma_{zc}}{E_c} + \alpha_c(\Delta T) \quad (B.20)$$

Cross sectional areas of core A_{co} , etched cladding A_{cl} and coating A_c can be respectively written as

$$A_{co} = \frac{\pi}{4} d_{co}^2$$

$$A_{cl} = \frac{\pi}{4} (d^2 - d_{co}^2),$$

$$A_c = \frac{\pi}{4} ((D)^2 - (d)^2)$$

where d_{co} -Core diameter

d -etched cladding diameter

D – Diameter after coating

For equilibrium of forces in the axial direction,

$$\sigma_{zco} A_{co} + \sigma_{zcl} A_{cl} + \sigma_{zc} A_c = 0 \quad (B.21)$$

From Equations B.20 and B.21,

$$\begin{aligned} & [\varepsilon_{eff} E_f - \alpha_{co} E_f (\Delta T)] A_{co} + [\varepsilon_{eff} E_f - \alpha_{cl} E_f (\Delta T)] A_{cl} + [\varepsilon_{eff} E_c - \alpha_c E_c (\Delta T)] A_c = 0 \\ & \varepsilon_{eff} [E_f A_{co} + E_f A_{cl} + E_c A_c] - \Delta T [\alpha_{co} E_f A_{co} + \alpha_{cl} E_f A_{cl} + \alpha_c E_c A_c] = 0 \\ & \varepsilon_{eff} = \Delta T \left[\frac{\alpha_{co} E_f A_{co} + \alpha_{cl} E_f A_{cl} + \alpha_c E_c A_c}{E_f A_{co} + E_f A_{cl} + E_c A_c} \right] \end{aligned} \quad (B.22)$$

This can be written in the form $\varepsilon_{eff} = (\Delta T) \alpha_{eff}$ where α_{eff} is the effective thermal expansion coefficient of the system.

$$\therefore \alpha_{eff} = \frac{\alpha_{co} E_f A_{co} + \alpha_{cl} E_f A_{cl} + \alpha_c E_c A_c}{E_f A_{co} + E_f A_{cl} + E_c A_c} \quad (B.23)$$

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To include the effect of the polymeric coating and the effect of cladding etching (if any) in the reflective spectrum of a uniform fiber Bragg grating, α_{eff} can be incorporated in Equation B.15. Therefore Equation B.15 $\Rightarrow$

$$\frac{dr}{dT} = \left[\frac{2 \sinh(\sqrt{k^2 - \dot{\sigma}^2} L) \cos(\sqrt{k^2 - \dot{\sigma}^2} L) \left(\cosh^2(\sqrt{k^2 - \dot{\sigma}^2} L) - \frac{\dot{\sigma}^2}{k^2} \right)}{\left(\cosh^2(\sqrt{k^2 - \dot{\sigma}^2} L) - \frac{\dot{\sigma}^2}{k^2} \right)^2} \right] \times \left[\left[\sqrt{k^2 - \dot{\sigma}^2} \alpha_{eff} L - L \left(\frac{\dot{\sigma}}{\sqrt{k^2 - \dot{\sigma}^2}} \left(\frac{2\pi\zeta_n n_{eff}}{\lambda} + \frac{\pi\alpha_{eff}}{\Lambda} \right) \right) \right] \right] - \left[\frac{2 \sinh^3(\sqrt{k^2 - \dot{\sigma}^2} L) \cosh(\sqrt{k^2 - \dot{\sigma}^2} L) \left(\sqrt{k^2 - \dot{\sigma}^2} \alpha_{eff} L - L \left(\frac{\dot{\sigma}}{\sqrt{k^2 - \dot{\sigma}^2}} \left(\frac{2\pi\zeta_n n_{eff}}{\lambda} + \frac{\pi\alpha_{eff}}{\Lambda} \right) \right) \right) \right]}{\left(\cosh^2(\sqrt{k^2 - \dot{\sigma}^2} L) - \frac{\dot{\sigma}^2}{k^2} \right)^2} \right] - \left[\frac{2 \dot{\sigma} \sinh^2(\sqrt{k^2 - \dot{\sigma}^2} L) \left(\frac{2\pi\zeta_n n_{eff}}{\lambda} + \frac{\pi\alpha_{eff}}{\Lambda} \right)}{\left(\cosh^2(\sqrt{k^2 - \dot{\sigma}^2} L) - \frac{\dot{\sigma}^2}{k^2} \right)^2} \right] \quad (B.24)$$

The above equation represents the temperature dependence of the reflectivity of a polymer coated uniform fiber Bragg grating. Lot of interesting features can be seen from the above equation. In addition to the grating parameters, the reflectivity depends on the temperature dependent material properties of the coating and also the coating thickness. It can be inferred that by selecting or controlling the material properties and by optimizing the coating thickness, it is possible to control the reflectivity and tune the center wavelength with respect to temperature.

Appendix C

MODELING THE EFFECT OF ADHESION OF POLYMERIC COATINGS

The adhesion of the polymeric coatings to the silica based optical fiber plays an important role in the wavelength response characteristics of fiber Bragg gratings with respect to temperature. In the present study, methodology based on finite element analysis is utilized for theoretical investigation of the effect of adhesion of polymeric coating on the performance of FBG based thermally tunable devices. The simulation methodology includes the utilization of one dimensional spring elements to connect the meshed models of optical fiber and the polymeric coating [151]. The effect of adhesion on the wavelength response properties with respect to temperature is expressed as a function of spring stiffness. Equivalent spring stiffness corresponding to uncoated FBG and ideal case is identified. The maximum achievable value of temperature sensitivity was found to be less than the theoretical maximum. The proposed methodology based on finite element analysis for the study of the effect of adhesion of polymeric coating on the performance of FBG based thermally tunable devices would be useful for characterizing the coating design, coating process control and evaluating its performance.

C.1 Methodology

Finite element analysis is carried using commercial software ANSYS 6.0. Three-dimensional model of the optical fiber and the silicone rubber coating is created using

solid elements. Brick element SOLID 45 is used for the modeling. The element is defined by eight nodes having three degrees of freedom at each node: translations in the nodal x, y, and z directions. Properties of Corning SMF-28 were assumed for the simulation. Length of the grating under consideration is 10 mm and an effective refractive index of 1.4682 was assumed with a uniform modulation of 10^{-4} in the effective refractive index.

In order to simulate various adhesion conditions, spring element COMBIN 14 is utilized. COMBIN14 has longitudinal or torsional capability in one, two, or three-dimensional applications. The element is defined by two nodes, a spring constant (k) and damping coefficients. The damping capability is not used for static or undamped modal analyses. The longitudinal spring constant should have units of Force/Length, the damping coefficient units are Force*Time/Length. Each node along the surface of the FBG is connected to the corresponding node at the coating surface by this one dimensional spring element as shown in Figure C.1. Longitudinal spring-damper option with UZ degree of freedom was selected.

Figure C.1. Schematic representation of contact condition between fiber and coating

A thermal load, which is equivalent to a temperature change of 100 °C, was applied for all the analysis. A thermo-optic coefficient of 8.6×10^{-6} was used for the analysis and its value is constant within the applied temperature range. Therefore the value of wavelength shift due to refractive index change remains constant and it is 1.33 nm for the applied temperature change of 100 °C. The wavelength shifts due to thermal expansion is computed for each analysis and thus the total wavelength shift can be determined. The wavelength shifts were calculated with respect to a center wavelength of 1550 nm.

C.2 Discussion

Figure C.2 shows the wavelength shifts at different levels of adhesion, which is expressed as function of spring stiffness, for a coating diameter $D=2$ mm. As shown in Figure, three well-defined zones can be identified. Region 1 represents the case of an uncoated fiber. This is analogous to the case of 0 % adhesion of polymeric coating material. The upper limit of this region is bounded by a spring stiffness value of 0.1 N/m. It can be seen that the wavelength shift is almost constant in this region and is equal to 1.4 nm/100 °C. This value matches with the observed wavelength shifts for bare or uncoated FBGs. In region 3 also, where the spring stiffness value is greater than 10^3 N/m, the wavelength shift remains constant at 2.27 nm. These values represent the ideal case of maximum achievable shift for the particular coating properties and configuration. This can be considered as a case of 100 % adhesion with no defects at the interface. It can be noted that this value is still less than the theoretical maximum of 2.76 nm as per the equations from Appendix B. Region 2 represents the actual case

where the spring stiffness lies in the range 10^{-1} to 10^3 . In this region, the percentage adhesion can be considered as varying from 0 to 100. Depending on that, the wavelength shifts varies from a minimum of 1.4 nm to a maximum of 2.27 nm. The experimental results of the temperature sensitivities lie in this zone.

Figure C.2 Effect of adhesion as a function of spring stiffness (D=2 mm). Figure C.3 Wavelength shift with respect to temperature (D=2 mm)

The wavelength shifts with respect to temperature is plotted in Figure C.3. The line with highest slope represents the theoretically calculated values according to equations in Appendix B. The shaded portion represents the enhancement in the temperature sensitivity due to the particular polymer coating. The lower range of the shaded area indicates the case of a bare FBG and the upper range represents the maximum possible values. The actual temperature sensitivity curve can lie anywhere in the shaded area depending upon the coating process quality, especially adhesion.

Figure C.4. Effect of adhesion as a function of spring stiffness (Coating diameter $D=10$ mm).

As seen from the theoretical analysis, exponential increase in the sensitivity can be achieved by increasing the coating thickness. This is also evident from the experimental studies. The FEA results for an FBG with coating diameter $D=10$ mm is shown in Figure C.4. The wavelength shift is expressed as a function of spring stiffness as in the previous case. Spring stiffness less than 0.1 N/m (region 1) represents the case of an uncoated fiber. That means the effect of coating is not utilized for enhancing the temperature sensitivity. Similarly region 3 ($k > 10^4$ N/m) shows the maximum enhancement in sensitivity that could be achieved practically. Therefore for a coating thickness of $D=10$ mm, sensitivity would be less than 9.5 nm/100 °C. The experimental result lies in Region 2.

Figure C.5. Effect of adhesion at various axial locations

Figure C.5 shows the effect of contact conditions at various levels of adhesion, the extent of adhesion being expressed in terms of percentage of adhered nodes. The stiffness $k=10^4$ was assumed for this analysis. It could be seen that wavelength shift is proportional to the number of adhered nodes between the fiber and the coating. The graph also shows the effect of adhesion at various axial locations of the grating with coating diameter $D=2$ mm. If the adhesive area is prominent only at the middle of the grating, for example, the wavelength shift is less compared to the adhesive area being concentrated at the grating ends. Also it could be seen that the shifts for adhesive area at the middle portion follows the same path as that of the grating with adhesive portions uniformly distributed along the axis of the FBG. If the percentage of adhered nodes is 40, the wavelength shift is 1.05 nm for adhesive areas at the end while it is only 0.65 nm for both uniform and centrally located adhesive areas. Therefore maximum care should be taken during the process control to keep the FBG ends fully adhered to the

coating. It can be concluded that the adhesion at the FBG ends are more important the point of view of obtaining maximum wavelength shifts.

C.3 Summary

The effect of adhesion of polymeric silicone rubber coating on the performance of FBG based thermally tunable devices, is modeled and analyzed. The simulation methodology includes the utilization of one dimensional spring elements to connect the meshed models of optical fiber and the polymeric coating. The effect of adhesion on the wavelength response properties with respect to temperature is expressed as a function of spring stiffness. Equivalent spring stiffness corresponding to uncoated FBG and ideal case is identified. For coating thicknesses in the range 1 mm to 5 mm, a stiffness value less than 0.1 N/m represents the bare FBG and a value greater than 1000 N/m represents the case of practical maximum. Also it is found that the adhesion at the FBG ends are more important from the point of view of obtaining maximum wavelength shifts.

APPENDIX D

SPECTRAL RESPONSE GRAPHS

Figure D.1 Spectral response of bare FBG at $E=5\text{ MPa}$ and $t=1\text{ mm}$ of the contact plate

Figure D.2 Spectral response of bare FBG at $E=5\text{ MPa}$ and $t=5\text{ mm}$ of the contact plate

Figure D.3 Spectral response of bare FBG at $E=50\text{ MPa}$ and $t=5\text{ mm}$ of the contact plate

Figure D.4 Spectral response of bare FBG at $E=500\text{ MPa}$ and $t=5\text{ mm}$ of the contact plate

Figure D.5 Spectral response of FBG at $E=5$ MPa and $D=175\text{ }\mu\text{m}$ of the coating

Figure D.6 Spectral response of FBG at $E=5$ MPa and $D=1000\text{ }\mu\text{m}$ of the coating

Figure D.7 Spectral response of FBG at $E=5$ MPa and $D=3000\text{ }\mu\text{m}$ of the coating

Figure D.8 Spectral response of FBG at $E=50$ MPa and $D=3000\text{ }\mu\text{m}$ of the coating

Figure D.9 Spectral response of FBG at $E=500$ MPa and $D=3000\text{ }\mu\text{m}$ of the coating

Figure D.10 Spectral response of bare FBG subjected to hydrostatic pressure

Figure D.11 Effect of a coating with $E=500$ MPa and $D=1000$ μm on HPT of FBG

Figure D.12 Effect of a coating with $E=500$ MPa and $D=5000$ μm on HPT of FBG

Figure D.13 Effect of a coating with $E=50$ MPa and $D=1000$ μm on HPT of FBG

Figure D.14 Effect of a coating with $E=5$ MPa and $D=1000$ μm on HPT of FBG

Figure D.15 Effect of a coating with $E=5$ MPa and $D=5000$ μm on HPT of FBG