Development of optical sensing based mems fabry- perot pressure sensors

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Development of Optical Sensing Based MEMS Fabry-Perot Pressure Sensors

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ABSTRACT

Optical metrology offers significant advantages in remote sensing and has been recently developed as an alternative to conventional piezoresistive and capacitive sensing techniques used for sensing applications capable of operating in harsh environments. Recent advancement in silicon micromachining techniques makes optical sensing devices more feasible for commercialization by reducing sizes and improving performance and manufacturability. Unfortunately, some common issues, such as signal-averaging effect induced by the non-planar pressurized deflection, cross-sensitivity to temperature and measurement errors arising from the process-induced variations, are major stumbling blocks facing the successful realization of this type of pressure sensors.

In this study, a high-performance single deeply corrugated diaphragm (SDCD) was developed to enable optical sensing technology to be better realized with MEMS technology. The key novelty marking the proposed SDCD is that it consists of a flat bottom-region that behaves as a normal flat diaphragm, and suspending sidewalls that serve as stress concentrator and buffer, thereby enhancing flatness of the diaphragm under pressurized deflection while reducing temperature dependence by releasing the thermally induced stress.

The non-planar deflection behavior of different types of sensing diaphragm was studied using FE analysis and the resultant signal averaging effects of different Fabry-Perot pressure sensors were modeled and simulated. Substantial reduction in signal averaging effect of Fabry-Perot pressure sensor using SDCD has been demonstrated. Based on the

coupled thermo-optical and stress-optical effects, thermally induced stress and its resultant thermal cross-sensitivity of a Fabry-Perot pressure sensor were modeled and evaluated. Analytical expressions were presented to analyze the stress reduction of the SDCC decoupling zone. Significant stress-release effect of an SDCC and the resultant low thermal cross-sensitivity of a SDCC pressure sensor have been validated.

On the other hand, for Fabry-Perot optical fiber sensors, the reflection and transmission characteristics of Fabry-Perot microcavity are normally analyzed using plane wave based techniques. However, a laser beam is Hermite-Gaussian in general and the difference between laser beam propagation features and those of a plane wave can lead to a considerable difference in the resultant optical response. To overcome such difficulty, a novel Gaussian optics based model was developed to accurately describe the behavior of Fabry-Perot microcavities illuminated by laser beams. It is well known that the performance of a Fabry-Perot pressure sensor is heavily influenced by the process-induced mirror layer thickness variations, which in turn are critically dependent on the fabrication process parameters. To better predict the effect of thickness variations, measurement error induced by manufacturing variations in mirror thicknesses has been investigated and an optimization model was successfully established to achieve both preferable sensing performance and least measurement tolerance for required high yield of sensors during batch fabrication.

Finally, manufacturing processes of the proposed Fabry-Perot pressure sensor using SDCC were developed. The prototype of the proposed sensor was successfully fabricated and characterized using a thermally incorporated optical measurement setup and results shown are very promising for the proposed sensor structure.

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NOMENCLATURE

Symbol	Description
E	Young's modulus
ν	Poisson's ratio
R	radius of a thin circular diaphragm
h	thickness of a thin diaphragm
a	half side length of a diaphragm, inner half side length of an SDCCD
H_d	depth of an SDCCD
α	angle of an SDCCD
d	thickness of an SDCCD
b	out half side length of an SDCCD
σ_0	initial stress of a thin diaphragm
P	external load
w	centre deflection of a thin diaphragm
S_m	mechanical sensitivity of a thin diaphragm
q	corrugation profile factor of a shallowly corrugated diaphragm
L	spatial corrugation period of a shallowly corrugated diaphragm
H_s	depth of a shallowly corrugated diaphragm
S	corrugation arc length of a shallowly corrugated diaphragm
C_{f1}	correction factor for a shallowly corrugated diaphragm

r	solidity ratio for a flat diaphragm with a rigid centre
A_{bp}	stiffness coefficient of a flat diaphragm with a rigid centre
B_{bp}	influence of a boss on the cubic item in the load-deflection equation of a flat diaphragm with a rigid centre
σ_i	initial stress of the i^{th} film layer
h_i	thickness of the i^{th} film layer
σ_{eq}	equivalent initial stress of a composite film stack
α	coefficient of thermal expansion
ρ	density
$C_1(H_d)$	correction factor on diaphragm stiffness for an SD CD
$C_2(H_d)$	correction factor on the initial stress for an SD CD
D	flexural rigidity of a diaphragm
S_o	optical sensitivity
$S_{overall}$	overall sensitivity of the whole pressure sensor
E	electrical vector in the electromagnetic field
H	magnetic vector in the electromagnetic field
R	reflectance of the thin film assembly
T	transmittance of the thin film assembly
θ_0	incidence angle
θ_i	incidence angle of the i th thin film layer
θ_s	refractive angle of the substrate

θ_g	refractive angle of cavity gap layer of Fabry-Perot microcavity
n_0	refractive index of the incident medium
n_i	refractive index of the i th mirror layer
n_s	refractive index of the substrate
n_g	refractive index of the cavity gap of Fabry-Perot microcavity
η_{fs}	optical admittance of the free space
η_0	optical admittance of the incident medium
η_i	optical admittance of the i th mirror layer
η_s	optical admittance of the substrate
τ	amplitude transmission coefficient
τ_m	amplitude transmission coefficient of the equivalent moving mirror interface of Fabry-Perot microcavity
τ_f	amplitude transmission coefficient of the equivalent stationary(fixed) mirror interface of Fabry-Perot microcavity
ρ	amplitude reflection coefficient
ρ_m	amplitude reflection coefficient of the equivalent moving mirror interface of Fabry-Perot microcavity
ρ_f	amplitude reflection coefficient of the equivalent stationary(fixed) mirror interface of Fabry-Perot microcavity
δ	phase shift experienced by the wave as it traverses the film
δ_i	phase shift experienced by the wave as it traverses the i th mirror layer
λ	optical wavelength

ω	beam size of the Gaussian beam
ω_0	beam waist size of the Gaussian beam
R_b	curvature radius of the Gaussian beam
A	intensity of the incident Gaussian beam
B_p	intensity of the p th transmitted beam
ψ'	Gaussian beam phase advance for one round trip in the air gap
Δx	transversal walkoff between successively transmitted beams
d_i	thickness of the i th mirror layer
g	Fabry-Perot cavity gap distance
Δg_i	manufacturing-induced thickness variation of Fabry-Perot cavity gap
Δd_i	manufacturing-induced thickness variation of i th mirror layer
Δg_{err}	induced measurement error of cavity gap Δg_{err}
Δg_{erm}	possible maximal error of Δg_{err}
ε_m	worst-case measurement accuracy
P_i	fraction of Fabry-Perot sensors with i th layer thicknesses between $d_i - \Delta d_i$ and $d_i + \Delta d_i$
P_g	fraction of Fabry-Perot sensors with initial cavity gap distances between $g_i - \Delta g_i$ and $g_i + \Delta g_i$
P_s	fraction of Fabry-Perot sensors with measurement accuracy not worse than ε_m
ε_{nl}	percentage nonlinearity of the measurand (i.e. transmittance) to the applied external loads

T_L	linear least square interpolation of the transmittance under different external loads
ε_{obj}	evaluation objective parameter
$\hat{T}$	modified transmittance of a Fabry-Perot microcavity with surface defects
ψ'_{ei}	perturbation or cavity length induced error of ψ' associated with the i th ideal elementary microcavity
A_{mi}	area of the i th infinitesimally divided Fabry-Perot mirror section
A_m	area of whole Fabry-Perot mirror
h_r	height of the irregularities on an ideal equivalent mirror surface of Fabry-Perot microcavity
σ_r	root-mean-square (RMS) value of the surface roughness
$\hat{R}$	modified reflectance of a Fabry-Perot pressure sensor with non-planar pressurized deflection
E^0	generalized Young's modulus
ε	thermally induced strain in thin film
ΔT	temperature variation
$\Delta \sigma$	thermally induced stress

Chapter 1

Introduction

1.1 Background

Owing to the potential advantages such as excellent sensitivity and resolution, immunity from electromagnetic interference, capability of multiplexing and dimensional compactness, extensive efforts have been devoted to the studies of optical fiber sensors during the past 15 years (J. Dakin *et al.*, 1988). These studies are applicable to structural integrity monitoring, acoustic emission sensing, impact and vibration detection and measurements of strain, temperature, pressure and various other physical quantities. Optical fiber sensors can offer the possible advantages that can be used in harsh environments, which are characterized by the existence of high temperature, severe vibration, electro-magnetic interference (EMI) and dust, etc. Such environments can possibly make electronics fail to operate. Furthermore, recent advances in micro-electro-mechanical system (MEMS) technology have made optical fiber sensors more feasible for commercialization through reduced cost and improved performance and manufacturability.

Normally, optical fiber sensors can be divided into two general classes, phase-modulated sensors and intensity-modulated sensors. Phase-modulated sensors measure differential phase changes in multi-path fiber geometries while intensity-modulated sensing technique measure environmentally induced changes in received power. On the other hand, among the various optical sensing techniques available, the Fabry-Perot

interferometry provides a high degree of sensitivity, versatility and immunity to environmental noise. Fabry-Perot microcavity-based elements have been widely used as temperature sensors, chemical sensors, tunable optical filters and amplifiers (J. P. Dakin *et al.*, 1987; J. Han, 1999; J. H. Jerman *et al.*, 1990; P. M. Sarro *et al.*, 1994). This kind of sensors detects changes in optical path length induced by either a change in the refractive index or a change in physical length of the cavity. Recently, Fabry-Perot optical fiber sensors have been exploited to detect the differential pressure as an alternative to conventional piezoresistive and capacitive pressure sensors to achieve high spatial resolution and to eliminate the problem of signal degradation encountered in harsh environments, such as blast-pressure measurement (W. N. Macpherson *et al.*, 2000) and turbulent-flow-pressure measurement in aerodynamics applications (M. J. Gander *et al.*, 2002).

For phase-modulated Fabry-Perot optical fiber sensors, although a high resolution of phase detection in the order of 10^{-6} rad can possibly be obtained, they require relatively complicated signal-processing techniques for effective signal recovery. Such signal-processing complexity involved greatly increases the costs of the sensors and makes them unacceptable for many applications. On the other hand, intensity-modulated Fabry-Perot optical fiber sensors require only simple signal processing. Although their sensitivities are limited by a small output range, as well as some other factors such as the shot noise of the light source and the fiber loss, these defects can be greatly reduced by optimizing the Fabry-Perot microcavity and by using higher-power laser sources.

Intensity-modulated optical fiber Fabry-Perot sensors have been developed and reported during recent years (Y. M. Kim *et al.*, 1995; J. Han *et al.*, 1996; D. C. Abeysinghe *et al.*, 2001). Different schemes of the deflection of moving mirror have been studied and different dielectric multilayers have been used as Fabry-Perot mirrors. However, there still exist several critical stumbling blocks facing the successful realization of this type of pressure sensors.

One of the major obstacles is the degradation of device performance arising from the non-planar deflection of the edge-clamped diaphragm/mirror when it is subject to external pressure load, which is often referred to as the signal-averaging effect. Although some mechanical improvements have been explored to reduce the signal averaging effect by maximizing the ratio between the deflecting diaphragm area and the optically sampled area and by adding corrugation structures to the deflecting diaphragm, these improvements are limited by the configurations of the sensors and they severely increase the complexity of the micromachining processes.

Another obstacle is the temperature dependence, which has always been a weakness of Fabry-Perot optical fiber sensors with conventional flat diaphragms. Stresses induced due to the mismatch in coefficients of thermal expansions (CTEs) among materials will fluctuate with ambient temperature and such fluctuation will disturb the sensor signal. Temperature dependence of the optical response of a Fabry-Perot optical fiber pressure sensor is closely related to material properties. Since it is difficult to eliminate the temperature dependence of the Fabry-Perot pressure sensor purely from the selections of materials, minimizing the thermal cross-sensitivity by selecting a proper diaphragm structure is believed to be one of the most effective ways.

On the other hand, for Fabry-Perot optical fiber sensor, the reflection and transmission characteristics of Fabry-Perot microcavities are normally analyzed using plane wave based techniques. Airy function (H. A. Macleod, 2001), two beam approximation (J. L. Santos *et al.*, 1992) and Macleod characteristic matrix technique (W. J. Wang *et al.*, 2004) are used to describe the behavior of Fabry-Perot microcavity of high finesse, low finesse and moderate finesse with multilayered mirrors, respectively. However, since it is well established that a laser beam is Hermite-Gaussian in general and is Gaussian in its fundamental mode TEM_{00} (H. Kogelnik *et al.*, 1966; J. T. Verdeyn *et al.*, 1989), the difference between laser beam propagation features and those of a plane wave can lead to considerable differences in the resultant optical responses. Therefore, it is imperative to develop a novel Gaussian optics based model to accurately describe the behavior of the Fabry-Perot microcavities illuminated by laser beams.

It is well known that the performance of a Fabry-Perot microcavity is greatly influenced by random film thickness variations (H. A. Macleod, 1976; P. Bousquet *et al.*, 1972; J. J. Monzon *et al.*, 1991). Therefore the output response of a Fabry-Perot pressure sensor is heavily influenced by the process-induced mirror layer thickness variations, which are critically dependent on fabrication process parameters, such as etching time and release process. Even though techniques of thickness control for thin films have been rapidly improved recently in practice, thickness variation is still very much a problem. In order to facilitate the commercialization of Fabry-Perot optical fiber sensors, Fabry-Perot microcavities should be optimized to achieve targeted performance stability for batch fabrication. Therefore, measurement errors induced by manufacturing variations in mirror thickness need to be investigated and an optimization model should be established to achieve both preferable sensing

performance and least measurement error for required high yield of sensors during batch fabrication.

1.2 Objectives

The objectives of the project are to (i) to develop a novel single deeply corrugated diaphragm (SDCD) for MEMS sensing applications, (ii) to establish a Gaussian optics based model and a comprehensive optimization model for Fabry-Perot microcavities with multilayered mirrors and, (iii) to characterize the extrinsic Fabry-Perot optical fiber sensor using SDCD. They include,

- Studies of the load-deflection behavior and the non-planar deflection behavior of SDCDs and other types of sensing diaphragms using both analytical method and finite element modeling.
- Studies of the dynamic characteristics of the proposed pressure sensor using SDCD by lumped models.
- Development of novel Gaussian optics based model for Fabry-Perot microcavity with multilayered mirrors illuminated by laser beams.
- Studies of the measurement error tolerance arising from process-induced variations in mirror layer thickness and development of an optimization model for Fabry-Perot microcavities for sensing applications.

- Studies of the perturbations of optical responses induced by non-normal incidence and different types of microcavity mirror imperfections.
- Studies of the stress reduction within the SDCD decoupling zone.
- Studies of the signal averaging effects and the thermal cross-sensitivities of Fabry-Perot optical fiber sensors using different types of sensing diaphragms.
- Development of micromachining processes for the fabrication of the proposed extrinsic Fabry-Perot pressure sensor using SDCD.
- Development of the whole thermally incorporated optical measurement setup to characterize the fabricated Fabry-Perot pressure sensors.

1.3 Outline of the report

This dissertation is organized as follows. Following the introduction in this chapter, current Fabry-Perot optical fiber sensors and corresponding analytical optical models are reviewed in Chapter 2 in which the motivations of this research are highlighted. In Chapter 3, the design of a novel single deeply corrugated sensing diaphragm for MEMS applications is presented in detail. The static load-deflection behavior of different types of diaphragms is analytically studied and the analytical results are confirmed by FEM modeling using ANSYS. The non-planar deflection behavior of a pressurized diaphragm and the dynamic characteristics of a pressure sensor using SDCD are further studied. Chapter 4 describes the novel optical modeling and optimization procedures developed for Fabry-Perot microcavities interconnected with

optical fibers. The perturbation of microcavity performance induced by non-normal incidence angle and microcavity mirror imperfections is also investigated in this chapter. In Chapter 5, analytical expressions are presented to analyze the stress reduction within the SDCCD decoupling zone. Two critical stumbling blocks facing the successful commercialization of an intensity-modulated Fabry-Perot optical fiber pressure sensor, i.e., signal averaging effect and thermal cross-sensitivity, are elaborately modeled and compared for pressure sensors using different deflected diaphragm schemes. Chapter 6 presents the manufacturing processes of the Fabry-Perot pressure sensor using SDCCD. In addition, the fabricated sensor is characterized using thermally incorporated optical measurement setup. Finally, conclusions and contributions are summarized in Chapter 7.

Chapter 2

Literature Review

2.1 Introduction

Optical sensing schemes have been described extensively in scientific literatures, and the following are some characteristic advantages of fiber optics that make them especially suitable for sensing applications:

- Non-electrical and non-contact
- Small size and light weight
- Allow access to harsh environments and remote operations
- Potentially easy to install and resistant to ionizing radiations
- Immune to radio frequency interference and electro-magnetic interference
- Solid-state reliability and high accuracy
- Interfacing capability with data communication systems
- Avoidance of ground loops and electric parks
- Capability of responding to a wide variety of measurands

Moreover, the advance of the field owes much to the development of the optical fibers and the associated optoelectronic devices for the telecommunications industry (B.

Culshaw *et al.*, 2004). Many optical fiber sensing structures capitalize upon the use of low cost sources, such as detectors and the specialized optical fibers that have been developed for other purposes. With the worldwide expansion of the fiber optical telephone trunk network for voice and data communications, there is now a wide availability for high quality optical and associated electronic components. The combination of these areas of expertise has led to many new developments in optical fiber sensing techniques.

2.2 Optical fiber sensors

Optical fiber sensors can be divided into two basic categories: phase-modulated and intensity-modulated sensors (T. G. Giallorenzi *et al.*, 1982). In general, intensity-modulated sensors are associated with displacements or some other physical perturbations that interact with fibers or mechanical sensing structures attached to fibers. These perturbations cause changes in received light intensities which are functions of the physical quantities to be measured. On the other hand, phase-modulated sensing techniques compare the phase of light in a sensing fiber to a reference fiber in a device known as an interferometer.

2.2.1 Intensity-modulated optical fiber sensors

Intensity-modulated sensors detect the amount of light that is a function of the perturbing environment. The light loss can be generally associated with transmission, reflection and microbending which can be incorporated in the fiber or in a reflective or transmissive target (D. A. Krohn, 2000). Intensity-modulated sensings normally require

more light energies to function than phase-modulated sensings and as a result, they use larger core multimode fibers (or bundles of fibers).

Fig.2.1 Schematic view of a loss-compensated fiber sensor for displacement measurement (A.Wang *et al.*, 1996)

A successful split-spectrum intensity-modulated (SSIB) optical fiber sensor is shown in Fig.2.1 (A. Wang *et al.*, 1996). The output of a broadband source is coupled into a multimode fiber and propagates through a 2×2 fiber coupler. The short-wave-pass interference edge filter, which is attached to or directly fabricated on the output end face of the optical fiber, reflects the portion of the spectrum above its cutoff wavelength and returns it directly to the fiber as reference signal. While the portion of the spectrum below filter edge wavelength transmits and propagates across the air gap to the mirror, referred to as the sensing signal. This portion of light is reflected again by the mirror and is then partially recoupled into the fiber. The two reflections will travel along the same fiber to the detector end and are separated spectrally by the wavelength division multiplexer (WDM) or demultiplexer. Since the intensity of the reference signal is

independent of the air gap in the sensor but the intensity of the sensing signal is a function of the air-gap separation, the power ratio of the sensing signal to the reference signal is thus a function of the air-gap separation.

For intensity-modulated optical fiber sensors, since the values of the physical measurands are directly derived from changes in light intensities, it becomes especially susceptible to power fluctuations of the light sources, fiber losses and variations in modal power distributions. All these factors will lead to changes in the resultant output of the sensor that could be wrongly interpreted as changes in the value of the actual measurand. Compensation of these changes is therefore a major concern when one is designing an intensity-modulated sensor. A considerable amount of research has been conducted in the past few years, such as using two light sources of the same wavelength (I. P. Giles *et al.*, 1985), two wavelength referenced technique for grating-type modulators (B. E. Jones *et al.*, 1984) etc. Similarly, for an SSIB sensor as shown in Fig.2.1, its advantage is that because the two signals travel along the same optical path in the fiber and are also radiated from the same source, the power ratio should therefore be immune to variations in both the fiber losses and the source power. However, when SSIB technique is used for sensing application, large diaphragm displacements (above $250\text{ }\mu\text{m}$) are required for pressure sensing up to about 1atm. Therefore, according to Wang's design as shown in Fig.2.2, large diaphragm size of diameter of 25mm is a prerequisite and this severely increases the size of the whole sensor.

Fig.2.2 Schematic view of SSIB pressure-sensor head (A. Wang *et al.*, 1996)

2.2.2 Phase-modulated optical fiber sensors

Phase-modulated sensors are the most extensively studied of all the fiber optic sensor concepts because of their extremely high sensitivities (A. R. Tebo, 1982). Generally, the sensor employs a coherent light source and two fibers. The light is first split and injected into each of the two fibers. If the environment perturbs one fiber relative to the other, a phase shift occurs and such phase shift can be detected by an interferometer very accurately.

There are four interferometric configurations which include the Mach-Zehnder, the Michelson, the Sagnac and the Fabry-Perot. Normally, Sagnac interferometer is mainly exploited for optical fiber gyroscope applications. For other sensing applications, because Mach-Zehnder and Michelson interferometric optical fiber sensors consist of two optical fibers for sensing and reference, both sensors are extremely sensitive to external disturbances. However, the Fabry-Perot interferometric optical fiber sensor,

which has only one guide optical fiber for incident and reflected lights, is independent of any disturbances within the guide fiber. Therefore, the Fabry-Perot sensor generally has twice as high sensitivity as compared with other techniques as discussed (D. A. Krohn, 2000) and provides a high degree of versatility and immunity to environmental noises (J. P. Dakin *et al.*, 1987; B. Halg, 1992; F. L. Degertekin *et al.*, 2001). Fabry-Perot sensing techniques have been broadly studied due to their capabilities for signal amplification (i.e. resonance) and tunability (M. A. Chan *et al.*, 1994; K. Aratani *et al.*, 1994). Moreover, Fabry-Perot sensing can be coupled to many physical parameters such as pressure and temperature. Because of the advantages of the Fabry-Perot sensing, it is extensively used in many MEMS sensing devices which will be highlighted in our later discussions.

2.3 Development of Fabry-Perot sensors

The Fabry-Perot interferometer is constructed of two reflective mirrors on either side of an optical medium. The interference results from successive reflections and transmissions of an initial beam between two reflectors as shown in Fig.2.3. The theory of the devices is reviewed in detail by Born (M. Born *et al.*, 1984) and Macleod (H. A. Macleod, 2001). Changing the spacing between the two mirrors causes a change of the final optical transmittance or reflectance. Moreover, the multiple passes along the fiber magnify the phase difference and result in an extremely high sensitivity. Overall interferometer performance is frequently characterized by a characteristic parameter called finesse $F = 4R/(1 - R)^2$, where R is the reflectance of the lossless reflectors. High finesse interferometers are useful because they have precisely located spectral features which can be used as optical filters, while low finesse interferometers allow

linear operations over a larger range of the physical parameter to be sensed without complex feedback schemes (J. L. Santos, 1992).

Fig.2.3 Fabry-Perot interference principle

The development of Fabry-Perot sensors began at the turn of the last century with derivatives of a parallel plate interferometer. Sensors for voltage and pressure were described by Perot and Fabry (A. Perot *et al.*, 1898). Of course, these sensors were constructed without taking the advantages of modern fibers and solid state light sources.

At the end of last century, optical fiber Fabry-Perot sensors emerged and were first applied to detect temperature changes. Early work on optic fiber Fabry-Perot sensors at the University of Washington (D. A. Christensen, 1974) demonstrated 99.5% accuracy for their temperature sensors with a measurement range of 150 degrees. Subsequent commercialization of optical fiber Fabry-Perot sensors has progressed from individually assembled prototypes to devices that can operate over an extended temperature range (K. A. James *et al.*, 1981; W. H. Quick *et al.*, 1983; J. W. Berthold *et al.*, 1987). Recently, due to its high spatial resolution and high sensitivity, Fabry-Perot technique is extensively exploited to detect the differential pressures for biomedical

applications (O. Tohyama *et al.*, 1998) and aerodynamics applications (M. J. Gander *et al.*, 2002). Therefore, optical fiber Fabry-Perot pressure sensor will become the central issue in the later discussions.

In general, optical fiber Fabry-Perot sensors can be broadly classified as either intrinsic Fabry-Perot sensors or extrinsic Fabry-Perot sensors. If all sensing mechanisms take place within the fiber itself, the sensor is classified as intrinsic. In this type of sensor, the fiber functions as both a transmission medium and sensor. An external parameter induces a change in the light guiding properties of the fiber which is detected and demodulated to produce measurand information. While in an extrinsic sensor, the fiber plays no role in the sensing mechanism and only serves to transfer the light to and from the modulated external medium in the sensor. The external medium can be either special optical crystals or air.

2.3.1 Intrinsic Fabry-Perot pressure sensors

In 1988, Lee and Taylor (C. E. Lee *et al.*, 1988) produced an intrinsic Fabry-Perot interferometer by introducing mirror coatings on a fiber by fusion splicing end-coated fibers. The advantage of an intrinsic Fabry-Perot sensor is that its spatial resolutions can be achieved on the micron scale, albeit at the cost of the requirement for complicated construction techniques (T. W. Kao *et al.*, 1996). An intensity-modulated intrinsic Fabry-Perot sensor was reported by Abeysinghe (D. C. Abeysinhe *et al.*, 2001), in which the cavity gap is formed by etched optical fiber end and anodically bonded silicon as shown in Fig.2.4. However, its complicated anodic bonding process and the resultant large losses for reflected light at the inter-connection severely limit its performance.

Fig 2.4 Schematic view of an intensity-modulated intrinsic Fabry-Perot pressure sensor
(D. C. Abeysinhe *et al.*, 2001)

Intrinsic Fabry-Perot sensor schemes using phase modulation have the advantages of ultra-high resolution, high accuracy and configuration versatility (T. G. Giallorenzi *et al.*, 1982). Three typical schemes are highlighted and are shown in Fig.2.5 (T. W. Kao *et al.*, 1996; J. Park *et al.*, 1999; W. N. Macpherson, 2000).

Fig.2.5 Different intrinsic Fabry-Perot pressure sensors proposed by (a) Kao (T. W. Kao *et al.*, 1996), (b) Park (J. Park *et al.*, 1999) and (c) Macpherson (W. N. Macpherson, 2000)

For sensors shown in Fig.2.5 (a) and (b), the optic fiber Fabry-Perot interferometer is composed of two internal mirrors separated by a gap distance. As longitudinal strain, which is induced by the deflected diaphragm, is applied to the interferometer, the round

trip phase difference is shifted because of the change in the optical cavity length. For the sensor shown in Fig.2.5(c), the Fabry-Perot microcavity is formed between the single mode fiber end and the deflected diaphragm. For all these structures, the reflectance of the mirror R is much less than 1 ($R \ll 1$) and this results in a low-finesse microcavity. With the assumption that the two mirrors are lossless, the reflectivity of a low-finesse microcavity can be approximated by a sinusoidal function based on two beam approximation (J. L. Santos *et al.*, 1992) as,

$$I = I_0[1 + V \cos(\phi - \phi_0)] \quad (2.1)$$

where I_0 is the input signal, V is the visibility of the interferometer, ϕ_0 is a phase constant and ϕ is the optical phase given by,

$$\phi = \frac{4\pi n L}{\lambda} \quad (2.2)$$

where n is the refractive index of the cavity, L is the cavity length and λ is the wavelength.

When the refractive index change of the optical fiber induced by strain-optical effect is considered, the pressure-induced phase change becomes (R. A. Atkins *et al.*, 1994),

$$\delta\phi = 0.78 \times \frac{4\pi n (\delta L)}{\lambda} \quad (2.3)$$

where δL is the change in the cavity length of interferometer and 0.78 is a correction value to account for the strain-optical effect.

However, for these intrinsic Fabry-Perot optical fiber sensors, they have the disadvantages of relative measurements and fringe direction ambiguities (K. A. Murphy *et al.*, 1992; R. O. Claus *et al.*, 1992). Although high sensitivities of 10^{-6} radian/Pa can be obtained, they require relatively complicated signal-processing techniques for effective signal recovery. Furthermore, if the cavity were interrogated with a single wavelength, the phase modulation would be restricted to no more than π radians, thus limiting the pressure range of sensors. By introducing additional wavelengths, the sensing range can be increased and the phase ambiguities can be eliminated (M. J. Gander *et al.*, 2002), however, the phase demodulation technique becomes more sophisticated (W. N. Macpherson *et al.*, 2000). The signal-processing complexity involved greatly increases the cost and makes them unacceptable for many applications.

2.3.2 Extrinsic Fabry-Perot pressure sensors

Extrinsic Fabry-Perot sensor was first proposed by Virginia Institute of Technology (K. A. Murphy *et al.*, 1992) and has been used widely nowadays such as measurement of the stiffness of ciliary bundles on hair cells (M. D. Barrett, 1999) and acoustic detection of traffic flow (N. Fustenau, 1998). For extrinsic Fabry-Perot sensors, there are two basic types of operation for these sensors depending on the size of the cavity gap.

One is the fringe-counting-based Fabry-Perot pressure sensor which has a large cavity length (length $> 10 \mu\text{m}$) and permits a large diaphragm pressurized deflection. The resultant optical light intensity has a multi-cycle operation and thus relies on fringe counting techniques for the determination of the measurand (V. Vali *et al.*, 1976). In this case, the Laser Emitting Diode (LED) is merely a broadband source that allows

tracking of the resonance peaks and valleys. This arrangement has been demonstrated to have successfully measured a number of physical parameters as discussed in (K. A. James *et al.*, 1981; W. H. Quick, *et al.*, 1983; K. L. Belsley *et al.*, 1986).

The other is the intensity-modulated Fabry-Perot pressure sensor which has small cavity length and hence, the maximum deflection allowable for the sensing diaphragm is strictly limited to one quarter of the wavelength ($\lambda/4$). Therefore, any shifts in the intensities can be directly and uniquely related to the magnitude of the physical parameter of interest. Compared with cumbersome and ambiguous fringe counting circuits associated with large-cavity Fabry-Perot interferometers, intensity-modulated Fabry-Perot pressure sensors have simpler optical intensity detection scheme (A. Mendez *et al.*, 1992), which makes it to be extensively studied in recent research. Also, these sensors require only simple signal processing. Although their sensitivities are limited by small output range and other factors such as shot noise of light source and fiber loss, these defects can be greatly reduced by optimizing the Fabry-Perot microcavity and by using higher-powered laser sources. Therefore, the extrinsic intensity-modulated Fabry-Perot pressure sensors have great potential to be commercialized and will be singled out in following discussions.

Miller (M. F. Miller *et al.*, 1997) presented a micromachined sensor array for making optical measurements of surface pressure distributions. Each sensor element in the array consists of a Fabry-Perot etalon fabricated using single-crystal silicon. Illuminating the sensor array using a near-infrared, tunable diode laser and detecting the reflected light intensity with an infrared camera can allow remote and simultaneous recording of the reflectance signals from the entire array of passive sensors. The sensor arrays are fabricated using a combination of bulk micromachining and wafer bonding

techniques with silicon-on-insulator (SOI) wafers being used as the working material from which the devices are formed. The Fabry-Perot etalon is formed between the lower surface of the pressure sensing membrane (Silicon) and the bottom of the etched cavity (Silicon) as shown in Fig.2.6. The anti-reflective coating on the top surface of the device which consists of a half-wavelength layer of SiO₂, reduces the reflective losses at the surface and prevents interference within the membrane. However, when the membrane deflects, the finesse of the cavity becomes increasingly degraded, leading to the reflectivity of the entire sensor to be rolled-off more quickly.

Fig.2.6 Micromachined Fabry-Perot pressure sensor for imaging surface pressure distributions (M. F. Miller *et al.*, 1997)

From Fig.2.6, it can be seen that one commonly used method of building a micro Fabry-Perot cavity is a hybrid assembly technique using a fusion bonding process. However, a high quality Fabry-Perot microcavity frequently requires an elaborate fusion bonding process, which may lead to low manufacturing yield. As an alternative, surface micromachined Fabry-Perot microcavity based devices have recently been developed. Among these devices are a tunable interferometer (K. Aratani *et al.*, 1994) and a pressure sensor (Y. M. Kim *et al.*, 1995). Surface micromachining techniques

eliminate the fusion bonding step and instead, they etch sacrificial layers to form high quality cavities with air gaps, achieving higher yield and better performance through precisely controlled thicknesses of the cavity gaps.

Kim (Y. M. Kim *et al.*, 1995) had successfully developed a surface micro-machined Fabry-Perot cavity that can be used as a pressure sensor. Using standard Integrated Circuit (IC) technology, dielectric film stacks consisting of silicon dioxide and silicon nitride were used as mirrors and polysilicon was used as a sacrificial layer that was then removed to form an air gap cavity as shown in Fig.2.7. The bottom diaphragm/mirror deflection induced by differential pressure results in a variation of the reflected optical light intensity which can then be detected.

Fig.2.7 Cross-sectional view of the Fabry-Perot cavity sensor with two flexible mirrors and an air gap (Y. M. Kim *et al.*, 1995)

For these two types of extrinsic intensity-modulated Fabry-Perot pressure sensor, the degradation of device performance arising from non-planar deflections of pressurized edge-clamped diaphragm is obvious. This is referred as signal-averaging effect and will severely deteriorate the sensor performance as applied external loads increase. Following Kim's research, Han (J. Han, 1999) used the top mirror as deflecting diaphragm and maximized the deflecting diaphragm size to enhance the flatness of the deflecting diaphragm within the optical sensing area to reduce signal averaging effect, as shown in Fig.2.8 (a). However, large deflecting diaphragm is more susceptible to sticking problem during release process and will increase the micromachining complexity. Therefore a shallow corrugation ring is added to the deflecting planar diaphragm to achieve better sensor performance (J. Han *et al.*, 2000), as shown in Fig.2.8 (b). Increasing the depth of shallow corrugation can further improve the flatness of the pressurized diaphragm and thereby reduce the signal averaging effect. Nevertheless, the shallow corrugation is also patterned on the air gap sacrificial layer, thereby limiting the corrugation depth to about $1\text{ }\mu\text{m}$ which in turn greatly limits the corrugation effect on reducing signal averaging effect.

Fig.2.8 Schematic view of Fabry-Perot pressure sensor using (a) Planar top defelecting diaphragm (J. Han, 1999) (b) Shallowly corrugated top defelecting diaphragm (J. Han *et al.*, 2000)

2.4 Existing optical models for extrinsic intensity-modulated Fabry-Perot pressure sensors

For an extrinsic intensity-modulated Fabry-Perot sensor, a low finesse results in a rather small output range which will decrease the sensor sensitivity, while a high finesse results in an optical response with a sharp peak which is also not suitable for sensing applications. Therefore, Fabry-Perot microcavity of moderate finesse is required for intensity-modulated sensing to provide required linearity and sensitivity. Since the mirrors of a moderate finesse Fabry-Perot microcavity are usually made up of dielectric layers, extensive study has been carried out on the optical response of an n-layer Fabry-Perot microcavity.

For many years, the n-layer problem in optics was considered to be intractable, until 1937, Rouard showed a method for closed form matrix solution in which multilayer structures can be analyzed by representing each layer as a 2×2 matrix (A. Nussbaum *et al.*, 1976; F. Abeles, 1967). Subsequent treatments have resulted in two different, but equivalent, formulations of Rouard's original method. One is the Macleod characteristic matrix technique (H. A. Macleod, 1976), which was developed from the basic Maxwell's equations. The other is the transfer matrix technique (O. S. Heavens, 1965) using matrix containing the Fresnel coefficients and the phase propagation across the layer. In 1958, Smith's method (S. D. Smith, 1958), known as the method of effective interfaces was presented which can isolate any layer or any combination of several consecutive layers and examine their influences on the performance of the whole thin film assembly. All these distinct techniques have been developed based on the superposition of plane waves. Among them, Macleod characteristic matrix technique is most extensively utilized to analyze the optical response of extrinsic

intensity-modulated Fabry-Perot microcavity for sensing applications (Y. M. Kim *et al.*, 1995; W. J. Wang *et al.*, 2004).

However, since Fabry-Perot microcavities are used in conjunction with laser beams for optical fiber Fabry-Perot sensors, the difference between laser beam propagation features and those of a plane wave can lead to a considerable difference in the resultant optical response. It is well established that a laser beam is Hermite-Gaussian in general and is Gaussian in its fundamental mode TEM_{00} (H. Kogelnik *et al.*, 1966; J. T. Verdeyn, 1989). Therefore, when a laser beam is used, the characteristics of a Fabry-Perot microcavity could not be simply evaluated by summing the plane waves obtained by multiple reflections from the interferometer surfaces. The passage of a laser beam through a Fabry-Perot cavity has been discussed in just a few papers and different theoretical approaches have been followed to derive an expression for the intensity of beam emerging from the cavity. It was first studied by summing the successively transmitted Gaussian beams of decreasing intensities (H. Abu-Safia *et al.*, 1994; Z. M. Wu *et al.*, 2003). Another method described the incident Gaussian beam by its plane-wave Fourier expansions and then calculated the corresponding spatial and spectral responses of a Fabry-Perot interferometer (F. Moreno *et al.*, 1992; E. Nichelatti *et al.*, 1995). However, these methods were applicable only to Fabry-Perot microcavities with mirrors of single layers and even so, they could not provide explicit analytical expressions. Therefore, it is imperative to establish a thoroughly analytical Gaussian optics based model to describe the optical response of a Fabry-Perot microcavity with multilayered mirrors.

2.5 Existing problems for extrinsic intensity-modulated Fabry-Perot pressure sensors

For extrinsic intensity-modulated Fabry-Perot pressure sensors, there still exist several problems facing their successful commercialization. These include signal averaging effect, thermal cross-sensitivity and measurement errors arising from the process-induced variations in mirror layer thickness.

a. Signal averaging effect

As we previously discussed, the non-planar deflection of an edge-clamped diaphragm under external loads will result in a degradation of the performance of the sensor, which is referred as signal-averaging effect. For extrinsic intensity-modulated Fabry-Perot sensors as shown in Fig.2.7-2.8, since the sizes of the deflected diaphragms are comparable with those of the optical fibers, the signal averaging effect will become more serious as external loads increase, leading to deteriorations of performances of sensors. As discussed in section 2.3.2, using the top diaphragm as a moving mirror and introducing shallow corrugation on the top diaphragm can alleviate signal averaging effect to some extent, however, the improvement is strictly limited by MEMS fabrication process. Since it is impossible to fabricate a freestanding, piston-like diaphragm in practice, the focus becomes to propose a novel sensing diaphragm structure to minimize the non-planarity of diaphragm deflection under applied external loads.

b. Thermal cross-sensitivity

Temperature dependence has always been a weakness of Fabry-Perot optical fiber sensors. For intensity-modulated extrinsic Fabry-Perot pressure sensors, mirrors are typically made of dielectric multilayered films. With a variation in ambient temperature, the thermal expansion induced thermal stress caused by the mismatch in coefficients of thermal expansion (CTEs) among materials lead to changes in both refractive index and film-layer thickness. Consequently, the quality and optical performance of the devices could become significantly deteriorated or even damaged owing to the phase shift or degradation. The temperature dependence of the center-wavelength shift of narrow-bandpass filters was recently reported in the literature (S. Sakaguchi, 1999; A. Zoller *et al.*, 1996).

However, for a pressure sensor, the induced thermal stress also causes diaphragm-stiffness variation and increased analysis complexity. Therefore, it is important to propose and provide a more reliable analytical model for evaluating the thermal stresses and then the thermal cross-sensitivity of a Fabry-Perot microcavity pressure sensor based on coupled thermo-optical and stress-optical effects. Since temperature dependence of the optical response of an intensity-modulated Fabry-Perot pressure sensor is closely related to material properties, it is difficult to eliminate the temperature dependence of the intensity-modulated Fabry-Perot pressure sensor only by selecting materials with appropriate thermal expansion coefficients. Therefore, a novel sensing diaphragm structure is also required to reduce the thermal cross-sensitivity for this type of sensor.

c. Measurement errors arising from the process-induced variations

It is well known that the performance of a Fabry-Perot microcavity with multilayered mirrors is greatly influenced by random film thickness variations (P. Bousquet *et al.*, 1972; H. A. Macleod, 1976; J. J. Monzon *et al.*, 1991). The process-induced mirror layer thickness variations are critically dependent on the fabrication process parameters such as etching time and release process. Even though the techniques of thickness control for thin films have been rapidly improved recently in practice, thickness variation is still very much a problem. Therefore, Fabry-Perot microcavity should be optimized to achieve targeted performance stability for batch fabrication.

A coating optimization method for Fabry-Perot interferometer was first presented by compromising between peak transmittance and fringe finesse (J. J. Monzon *et al.*, 1993). Also, by selecting proper initial cavity gap distance and mechanical travel distance, the optimization of a Fabry-Perot sensor was performed to enlarge the yield of devices that meet specifications (Y. M. Kim *et al.*, 1995). However, these optimizations are based on plane-wave characteristic matrix thin-film optics and they fail to consider some important performance indices of sensors, such as percent nonlinearity error and sensing resolution. Therefore, a novel optical optimization model is required to achieve both the preferable sensing performance and the least measurement tolerance for high yield of sensors during batch fabrication.

2.6 Summary

Different types of optical fiber sensor developed to date have been reviewed and extrinsic intensity-modulated Fabry-Perot pressure sensor is singled out for our studies

because of its high spatial resolution, simple signal demodulation, absolute measurement and single-chip fabrication. However, it is evident that the signal averaging effect, thermal cross-sensitivity and measurement errors arising from the process-induced variations in mirror layer thickness are the three major problems that affect the performance of this type of sensor. Therefore it is essential to develop a novel sensing diaphragm structure for MEMS optical sensing applications, which can perform piston-like deflection under external loads while significantly releasing the induced thermal stress in sensing area at the same time. Also, existing plane-wave based optical models have been found to be not accurate enough when they are used to describe the Fabry-Perot microcavity illuminated by laser beams. And the available Fabry-Perot microcavity optimization models and the thermal cross-sensitivity models have been found to be not comprehensive enough to be applied to analyze the proposed extrinsic Fabry-Perot pressure sensors.

These are therefore the motivations and objectives of the research project described in this dissertation. Following the critical review of existing work, a novel single deeply corrugated diaphragm (SDCD) is proposed as a sensing diaphragm for MEMS optical sensing applications. Then, the Fabry-Perot microcavity illuminated by laser beam is analytically modeled using Gaussian beam optics and Smith's method. Also, measurement errors arising from the process-induced variations are investigated and a comprehensive optimization model is established to achieve both the preferable sensing performance and the least measurement tolerance for high yield of sensors during batch fabrication. The signal averaging effects of Fabry-Perot optical fiber sensors using different types of sensing diaphragms are investigated and compared. The stress release effect of SDCCD decoupling zone is analytically studied and the thermal cross-

sensitivity of the proposed sensor is evaluated based on coupled thermo-optical and stress-optical effects. Finally, the proposed sensor is successfully fabricated and characterized.

Chapter 3

Mechanical Design of the Proposed Sensor

3.1 Introduction

The vast majority of micromachined silicon diaphragms are flat and are either square or rectangular, which can be used as the sensing element to detect various physical measurands such as pressure and temperature. Several papers have been published regarding the deflection characteristics of micromachined diaphragms for applications to pressure sensors, acoustic devices and optical interference structures (R. Schellin *et al.*, 1994; D. Maier-Schneider *et al.*, 1995; H. E. Elgamel, 1995; R. Steinmann *et al.*, 1995). For these applications, large and linear travel of diaphragm structures is desired. However, two major problems exist for conventional flat diaphragms. One is the non-linearity caused by a considerable initial stress encapsulated in the diaphragm, which becomes appreciable when pressure range to be measured is very low. The other is the non-flatness of the pressurized deflection for edge-clamped diaphragm, which results in the degradation of the output response, especially in the case of optical sensing. Therefore, it is preferable to use some more elaborate diaphragm schemes to realize the desirable linear and piston-like travel in the existence of initial stress. In order to avoid expensive “trial and error” process runs, especially in MEMS since micromachining processes are usually very expensive, it is important to be able to predict the mechanical behavior of the diaphragm and to optimize its design according to sensing application requirements (J. H. Correia, 1999). For this purpose, analytical solutions for

different sensing diaphragm behavior are desirable because of their ease to use and the insight they provide to the designer.

In this chapter, the load-deflection behavior of four different sensing diaphragm designs, i.e., the flat diaphragm, the shallowly corrugated diaphragm, the flat diaphragm with rigid center and the SDCD, will be analytically investigated. Both mechanical sensitivities and non-planar behavior under pressurized deflections are studied by analytical solutions and FEM simulations through commercial software ANSYS. The elaborate mechanical design of a pressure sensor with an SDCD is then presented.

3.2 Load-deflection behavior of conventional sensing diaphragms

3.2.1 Flat diaphragm

Although numerical techniques, such as Finite Element Analysis, Boundary Element Analysis, and Finite Difference Analysis, can be more accurate in predicting deflection behavior, especially for large deflections, these techniques generally require more effort to use and may not supply the same physical insight as analytical or exact variational solutions do (W. P. Eaton *et al.*, 2000). The use of plate theory is appropriate for the analysis of micromachined thin-film diaphragms (S. Timoshenko *et al.*, 1987). Small deflection theory is often used based on the following restrictive assumptions (M. D. Giovanni, 1982):

- (1) The plate is flat and is of uniform thickness

- (2) The material is isotropic and homogeneous
- (3) The maximum deflection due to applied pressure should be small – not more than 30% of the plate thickness
- (4) All forces, loads are applied normally to the plane of the plate
- (5) The plate is not stressed beyond its elastic limit
- (6) The plate should not be too thick. For a round diaphragm, its thickness should not be more than 20% of the diameter.
- (7) The plate deflection is due mostly to bending; therefore, the neutral plane of the plate endures no tensile stresses.

Based on small deflection theory, the centre deflection w of a flat circular diaphragm with clamped edges in the case of free initial stress and under a homogeneous pressure distribution, P , can be calculated as (C. W. Bert *et al.*, 1988),

$$\frac{PR^4}{Eh^4} = \frac{5.33}{(1-\nu^2)} \frac{w}{h} + \frac{2.83}{(1-\nu^2)} \frac{w^3}{h^3} \quad (3.1)$$

where E, ν, R and h are the Young's modulus, the Poisson's ratio, the radius and the thickness of the diaphragm, respectively. Likewise, the equivalent square diaphragm deflection is given by (H. L. Chau *et al.*, 1987),

$$\frac{Pa^4}{Eh^4} = \frac{4.20}{(1-\nu^2)} \frac{w}{h} + \frac{1.58}{(1-\nu^2)} \frac{w^3}{h^3} \tag{3.2}$$

where a is the half side length.

Based on Eq.(3.2), the load deflection behavior of a flat square diaphragm is simulated as shown in Fig.3.1. The diaphragm parameters used in the simulation are listed in Table 3.1. The diaphragm is a *polySi/SixNy/polySi* film stack with a thickness of 500nm/500nm/500nm and Young’s Modulus E can be assumed to be 200GPa. The value of Young’s modulus E will be later validated in section 3.3.2. It can be seen from Fig.3.1 that flat diaphragms show a nonlinear relation between the deflection and the applied load pressure. When $w/h \ll 1$, the relation between the centre deflection and the applied pressure is virtually linear and the cubic item in Eq.(3.2) can be neglected. However, substantial nonlinearity occurs when deflections reach more than 25% of the thickness of the diaphragm and becomes increasingly significant with the increase of the applied pressure.

Table 3.1 Parameters used in load-deflection behavior simulation of the square flat diaphragm

Description	Diaphragm
Thickness	1.5 μm
Young’s Modulus, E	200GPa
Poisson’s ratio, ν	0.3
Half side length	50 μm

Fig.3.1 Load-deflection behavior of square flat diaphragm without initial stress

Under large values of initial tensile stress σ_0 , the centre deflection w of a flat circular diaphragm can be computed from (J. H. Jerman *et al.*, 1990):

$$\frac{PR^4}{Eh^4} = \frac{4\sigma_0 R^2}{Eh^2} \frac{w}{h} \quad (3.3)$$

When a flat square diaphragm ($2a \times 2a$) under high stress is considered, R^2 should be replaced by an equivalent $4a^2/\pi$ (Q. B. Zou *et al.*, 1996) and hence Eq.(3.3) is rewritten in this case as:

$$\frac{Pa^4}{Eh^4} = \frac{\sigma_0 \pi a^2}{Eh^2} \frac{w}{h} \quad (3.4)$$

The centre deflection of a flat square diaphragm under initial stress can therefore be approximated by superposition of the bending resistance to initial stress, i.e., the right hand side of Eq.(3.4), and the term in the right hand side of Eq.(3.2) as:

$$\frac{Pa^4}{Eh^4} = \left(\frac{4.20}{(1-\nu^2)} + \frac{\sigma_0 \pi a^2}{Eh^2} \right) \frac{w}{h} + \frac{1.58}{(1-\nu^2)} \frac{w^3}{h^3} \quad (3.5)$$

Similarly, when small deflections are considered, we can omit the cubic item in Eq.(3.5) and define the diaphragm mechanical sensitivity as the ratio between the diaphragm centre deflection w and the applied pressure P ,

$$S_m = \frac{w}{P} = \frac{a^2}{\pi h \left(\frac{4.2Eh^2}{\pi a^2(1-\nu^2)} + \sigma_0 \right)} \quad (3.6)$$

For the diaphragm whose dimensions and material properties are given in Table 3.1, the mechanical sensitivity under different initial stress is simulated and shown in Fig.3.2. From Fig.3.2, it is clear that the mechanical sensitivity decreases with the increases of the stress σ_0 , and as a result, a higher initial stress would result in a lower mechanical sensitivity. Furthermore, since the initial stress in silicon diaphragm is determined by the diaphragm fabrication processes, mechanical sensitivity is hence difficult to reproduce in practise, if the process is not well controlled. This poor reproducibility becomes a major obstacle to the applications of flat diaphragms in pressure sensors and other acoustic devices.

Fig.3.2 Mechanical sensitivity of square flat diaphragm under different initial stresses

3.2.2 Shallowly corrugated diaphragm

It has been recognized for decades in the field of conventionally formed metal diaphragms that by introducing corrugations into the diaphragm structure, the load-deflection linearity of a diaphragm can be increased considerably (W. A. Wildhack *et al.*, 1939). Based on this observation, some work has been undertaken to transfer conventional corrugated diaphragm technology to micromachined silicon structures (J. R. Mallon *et al.*, 1984). The schematic diagram of shallowly corrugated diaphragm is shown in Fig 3.3.

Fig.3.3 Schematic diagram of a shallowly corrugated diaphragm

In 1957, Haringx (J. A. Haringx, 1957) first presented a model to calculate the deflection of circular, stress-free corrugated diaphragms. After that, Giovanni (M. D. Giovanni, 1982) presented another set of equations which were essentially identical to the equations presented by Haringx for small diaphragm deflections, but they did provide a more accurate solution for the case of large deflections as,

$$P = 4 \frac{h^2}{R^2} \frac{w}{h} \left[\frac{A_p}{4} E \frac{h^2}{R^2} + \frac{B_p}{4} \frac{E}{1-\nu^2} \frac{h^2}{R^2} \frac{\omega_0^2}{h^2} \right] \quad (3.7)$$

where R , h and E are the radius, thickness and Young's modulus respectively, while A_p and B_p are expressed as,

$$A_p = \frac{2(q+1)(q+3)}{3 \left(1 - \frac{\nu^2}{q^2} \right)} \quad (3.8)$$

$$B_p = 32 \frac{1-\nu^2}{q^2-9} \left[\frac{1}{6} - \frac{3-\nu}{(q-\nu)(q+3)} \right] \quad (3.9)$$

$$q^2 = \frac{S}{2L} \left(2 + 3 \frac{H_s^2}{h^2} \right) \quad (3.10)$$

where ν is Poisson's ratio, H_s is the depth of the corrugation, L is the spatial corrugation period, S is the corrugation arc length and q is the corrugation profile factor. In Eqs.(3.7) to (3.10), it is assumed that the corrugated diaphragm is full of shallow corrugations and does not have a flat centre zone. The deflection of a corrugated diaphragm with initial stress can be approximated by means of an analytical expression, which is a superposition of the linear model of a corrugated diaphragm without initial stress and the linear flat membrane model with initial tensile stress (P. R. Scheeper *et al.*, 1994). The superposition is based on the assumption that the corrugated diaphragm can be modeled as an equivalent flat diaphragm which locally has the same radial and tangential flexural rigidity as the corrugated diaphragm. Since the initial stress is reduced by the presence of corrugations in the diaphragm by a factor of $B_p / 2.83$ (P. R. Scheeper *et al.*, 1994) and the superposition is made only for the linear terms, the following equation is obtained:

$$P = 4 \frac{h^2}{R^2} \frac{w}{h} \left[\sigma_0 \frac{B_p}{2.83} + \frac{A_p}{4} E \frac{h^2}{R^2} \right] \quad (3.11)$$

where σ_0 is the initial stress of the diaphragm layer without corrugation.

Similarly, for a square diaphragm ($2a \times 2a$) with shallow corrugations, the relation between w and P can be approximated by replacing R^2 in Equation (3.11) by $4a^2/\pi$,

$$S_m = \frac{w}{P} = \frac{a^2}{\pi h \left(\sigma_0 \frac{B_p}{2.83} + \frac{\pi A_p}{16} E \frac{h^2}{a^2} \right)} \quad (3.12)$$

In order to keep the vibrating membrane as a whole and firmly connected with the substrate, part of the diaphragm area has to be uncorrugated in order to connect all the corrugation portions together. Such area is called bridge as shown in Fig.3.4. Therefore, correction factor C_{f1} has to be introduced to consider the effect of the bridges and Eq.(3.12) becomes,

$$S_m = \frac{w}{P} = \frac{a^2}{\pi h \left(\sigma_0 \left(\frac{B_p}{2.83} + C_{f1} \right) + \frac{\pi A_p}{16} E \frac{h^2}{a^2} \right)} \quad (3.13)$$

Fig 3.4 Schematic description of bridges in shallowly corrugated diaphragms

The correction factor C_{f1} can be determined either by numerical analysis such as FE analysis or through experimental measurement. Fig.3.5 shows the mechanical sensitivity of the shallowly corrugated diaphragm with different corrugation depths and under different initial stresses of 120MPa, 160MPa and 200MPa. The basic diaphragm parameters are listed in Table 3.1 and two diaphragm sizes, $100 \times 100 \mu m^2$ and

$1000 \times 1000 \mu m^2$ are used in the simulation. The corrugation depths are assumed to be up to $2 \mu m$ for the $100 \times 100 \mu m^2$ diaphragm and $10 \mu m$ for the $1000 \times 1000 \mu m^2$ diaphragm, respectively. It can be seen from Eq.(3.12) that the mechanical sensitivity of the diaphragm with shallow corrugations can be improved by varying the coefficients B_p and A_p . This can be achieved by increasing the q value with a larger corrugation depth. However, an increase in corrugation depth will cause coefficient A_p to increase rapidly while B_p to decrease rapidly. In this case, although the effect of the initial stress is reduced by a much lower value of coefficient B_p , a larger weightage A_p is factored to the Young's modulus, leading to an increase in diaphragm stiffness. Therefore, there exists a trade-off between the reduction in initial stress and the increase in diaphragm stiffness for shallowly corrugated diaphragm design and this imposes limits on the improvement of mechanical sensitivity.

These effects are demonstrated clearly as shown in Fig.3.5, in which the mechanical sensitivities firstly increase to the maximum and then decrease. However, it also can be seen from Fig.3.5(b) that for the case of a small diaphragm size of $100 \times 100 \mu m^2$, the improvements of the mechanical sensitivities by increasing the corrugation depth are greatly limited. This is because for a small diaphragm of half side length a , the $\frac{\pi}{16} E \frac{h^2}{a^2}$ term in Eq.(3.13) is much greater than that of a large diaphragm. Therefore, when compared with a large diaphragm, the increase in the stiffness of a small diaphragm becomes more evident when corrugation depth increases and as a result, the improvement of the mechanical sensitivity benefited from the decrease of the initial stress effect is weakened.

Fig.3.5 Mechanical sensitivity of a flat square diaphragm with shallow corrugations of different depths and under different initial stresses for (a) $1000 \times 1000 \mu\text{m}^2$ (b) $100 \times 100 \mu\text{m}^2$

3.2.3 Flat diaphragm with rigid center

As illustrated before, bossed regions can be fabricated on the backside of the diaphragm to diffuse stress to the periphery to produce a piston-like travel for the

central region of the diaphragm. The schematic diagram of a flat diaphragm with rigid center is shown in Fig.3.6.

Fig.3.6 Schematic diagram of a flat diaphragm with a rigid center

The deflection of a square diaphragm with a rigid center under pressure can be expressed by the following equation (M. D. Giovanni, 1982),

$$\frac{Pa^4}{Eh^4} = \frac{1}{A_{bp}} \frac{4.20}{(1-\nu^2)} \frac{w}{h} + \frac{1}{B_{bp}} \frac{1.58}{(1-\nu^2)} \frac{w^3}{h^3} \quad (3.14)$$

where A_{bp} is a stiffness coefficient. Assume that the half side length of the diaphragm and the rigid centre are a and b , respectively, the solidity ratio r of a flat diaphragm with a rigid centre can be defined as,

$$r = b/a \quad (3.15)$$

And the stiffness coefficient A_{bp} depends on the solidity ratio as,

$$A_{bp} = 1 - r^4 - 4r^2 \log \frac{1}{r} \quad (3.16)$$

The influence of a boss on the cubic term B_{bp} is much more complicated and will not be discussed in detail here since it can be omitted when small deflection is under consideration which is a case for our intended application. A quite extensive analysis of B_{bp} can be found in the work of R. H. Liu (R. H. Liu, 1985).

Similarly, when a flat square diaphragm with a rigid centre and under initial stress σ_0 is considered, based on supposition, Eq.(3.14) can be rewritten as:

$$\frac{Pa^4}{Eh^4} = \left(\frac{1}{A_{bp}} \frac{4.20}{(1-\nu^2)} + \frac{\sigma_0 \pi a^2}{Eh^2} \right) \frac{w}{h} + \frac{1}{B_{bp}} \frac{1.58}{(1-\nu^2)} \frac{w^3}{h^3} \quad (3.17)$$

When small deflection is considered, the diaphragm mechanical sensitivity can be defined as,

$$S_m = \frac{w}{P} = \frac{a^2}{\pi h \left(\frac{1}{A_{bp}} \frac{4.2Eh^2}{\pi a^2(1-\nu^2)} + \sigma_0 \right)} \quad (3.18)$$

From Eq.(3.18) it can be seen that with the increase of the size of the rigid centre, the mechanical sensitivity will be reduced because of the increase in the overall stiffness of the whole diaphragm. It is obvious that the mechanical sensitivity of a flat diaphragm with a rigid centre is smaller than that of an equal-sized flat diaphragm and the ratios between them under different solidity ratio r and initial stresses σ_0 are shown in the Fig.3.7. The mechanical properties of the diaphragm used in the simulations are listed in Table 3.1. It can be seen from Fig.3.7 that, as compared with a normal flat diaphragm, the mechanical sensitivity of a flat diaphragm with a rigid center is reduced

by about 35% for the case of stress-free and by about 25% for the case of an initial stress of 200MPa. Therefore, although a flat diaphragm with a rigid centre can increase the degree of flatness of the central diaphragm under pressurized deflection, it comes with the price of a reduction of mechanical sensitivity.

Fig.3.7 Mechanical sensitivity ratio between a square flat diaphragm with a rigid centre and an equal-sized flat diaphragm under different solidity ratio and initial stresses

3.3 Load-deflection behavior of a novel SDCD

The novel SDCD consists of a flat bottom-region that is suspended all around with free-sidewalls, as shown in Fig.3.8. SDCD was first proposed by our research group for its application in a silicon condenser microphone with improved sensitivity (X. X. Li *et al.*, 2001). Although there has been no reported analytical expression for this type of diaphragm in open literature, it is believed that it should behave like a flat diaphragm,

at least for the region at the bottom of the corrugation. Taking into account the effect of the large corrugation depth, correction factors $C_1(H_d)$ and $C_2(H_d)$, which are dependent on corrugation depth H_d , are introduced into the basic equation of (3.6). The empirical relation between the diaphragm centre deflection and the applied pressure for a square diaphragm with a single deep corrugation can be approximately written as:

$$S_m = \frac{w}{P} = \frac{a^2}{\pi h \left(C_1(H_d) \frac{4.2Eh^2}{\pi a^2(1-\nu^2)} + C_2(H_d)\sigma_0 \right)} \quad (3.19)$$

Fig.3.8 Schematic cross-section view of an SDCCD

For a diaphragm with a single deep corrugation, both $C_1(H_d)$ and $C_2(H_d)$ are believed to be smaller than 1 and decrease with the increase in corrugation depth H . The effect of the initial stress on mechanical sensitivity of the diaphragm will thus be minimised with lower diaphragm stiffness. Hence, a higher mechanical sensitivity can be expected with this type of diaphragm. To prove this argument, correction factors, $C_1(H_d)$ and $C_2(H_d)$, will be studied using finite element analysis. Before finite

element analysis, the geometrical dimensions and material properties of the diaphragm used in the simulation will be introduced first.

3.3.1 Design parameters and properties of the diaphragm

Due to the existence of initial stress of deposited films, a deposited thin film can suffer from cracking or buckling. To prevent these unwanted behaviors, a multiple film stack, consisting of films under both compressive initial stress and tensile initial stress can be used. The resultant composite film will have less resultant initial stress due to the cancellations of the compressive and the tensile stresses of different layers of films (Y. M. Kim *et al.*, 1995). Thin-film silicon-compatible materials, such as polysilicon, LPCVD (low-pressure chemical vapor deposition) low-stress silicon-rich silicon nitride, have good optical characteristics. They are highly transparent and the optical absorption of these dielectric films can be omitted. Based on this principle and considering the film's optical characteristics as well as compatibility with micromachining of MEMS devices, we proposed and developed a composite layered thin film consisting of two polysilicon layers with one silicon nitride layer (*polySi/SixNy/polySi*) for optical sensing. It should be pointed out here that for silicon-rich silicon nitride (Si_xN_y), x and y value are determined by LPCVD process.

The equivalent initial stress of a composite film stack can be estimated based on the following formula:

$$\sigma_{eq} = \frac{\sum_i (\sigma_i \cdot h_i)}{\sum_i h_i} \quad (3.20)$$

where σ_i is the initial stress and h_i is the thickness of the i^{th} layer. The thickness of each layer should be chosen in such a way that the equivalent stress of the film stack is minimized but remains positive so that the composite film stack is slightly under tension. Normally, a LPCVD polysilicon layer has a compressive stress of about 270MPa (H. Kahn *et al.*, 1996), and a LPCVD silicon nitride layer has a tensile stress of about 540MPa (J. G. E. Gardeniers *et al.*, 1996). Therefore, the designed thickness for *polySi/SixNy/polySi* film stack can be chosen as 500nm/500nm/500nm which is also appropriate for MEMS fabrication.

For such sandwiched diaphragm, Young's Modulus E can be assumed to be 200GPa, while for single layer silicon nitride and polysilicon, Young's modulus are about 290GPa and 160GPa, respectively. Poisson's ratios ν of all the materials are set to be 0.3 in simulations and the subsequent analysis of results. Table 3.2 tabulates the composite diaphragm material properties used in FE simulation.

In all the simulations, initial stress was induced in the diaphragm by the use of the "cooling temperature" method as proposed by Zhang and Wise (Y. Zhang *et al.*, 1994). For an initial stress of σ_0 , an equivalent temperature difference becomes,

$$T = \frac{(1-\nu)\sigma_0}{E\alpha} \quad (3.21)$$

where T is the temperature, ν is the Poisson's ratio, σ_0 is the induced initial stress, E is the Young's modulus of elasticity and α is the coefficient of thermal expansion. In ANSYS simulation, stress stiffening effects can be activated and the pre-stressed film can be analyzed for its mechanical sensitivity and natural frequency.

Table 3.2 Properties of composite diaphragm in FE simulation

Description	Diaphragm
Materials	polySilicon/ Si_xN_y /polySilicon
Thickness	$1.5\ \mu m$ (500nm/500nm/500nm)
Size	$100\ \mu m \times 100\ \mu m$
Young's Modulus, E	200GPa
Poisson's ratio, ν	0.3
Density, ρ	$2200\ kg/m^3$

3.3.2 Verification of ANSYS simulation results

To verify the accuracy of the simulation results as well as the equivalent suppositions, mechanical sensitivity of a flat diaphragm is simulated and compared with its respective analytical solutions. Due to the symmetry of the diaphragm under consideration, only one quarter of the whole structure is analyzed in order to reduce model complexity and computation time, as shown in Fig.3.9. Appropriate boundary conditions for edge-clamped diaphragm must be imposed before simulation. Also, the boundary conditions of the nodes lying along the boundary between modeled and non-modeled regions can be derived based on the symmetry of the motion and the geometry of the structure.

In ANSYS verification, the sandwiched diaphragm is first modeled as one layer with equivalent Young's modulus, density and initial stress using three-dimensional shell

element SHELL63. Single layer thickness of $1.5 \mu m$ is used as SHELL63 Real Constants. Then another composite structural shell model SHELL99 is used in the simulation to validate the equivalent suppositions, such as equivalent Young's modulus for the sandwiched diaphragm. For SHELL99 element, each layer in the diaphragm can be defined with different Young's modulus, Poison's ratio and layer thickness through Real Constants. Both simulations are performed for the square diaphragm under different initial stresses from 0 to 200MPa and compared with analytical results obtained from Eq.(3.6) and the results are shown in Fig.3.10. It can be seen from Fig.3.10 that compared with analytical results, the maximum deviations for simulated results using SHELL63 and SHELL99 are 7.7% and 4.3%, respectively. These results validate that the proposed sandwiched diaphragm can be effectively modeled as one layer structure using equivalent supposition which is adequately accurate in FEM analysis for the structural designs of diaphragms.

Fig.3.9 ANSYS modeling of one quarter edge-clamped square diaphragm

Fig.3.10 Mechanical sensitivities simulated by different FE analyses and analytical method for a square flat diaphragm under different initial stresses

3.3.3 Modeling of an SDCD

For an SDCD, it should be pointed out that angle α shown in Fig.3.8 should be 54.74° due to the anisotropic etching process used. It is because silicon wafer with crystal orientation of (100) and with a square opening will be etched at an angle of 54.74° with respect to its horizontal axis when exposed to Potassium hydroxide solution (KOH) during micromachining process. Therefore, the width of the corrugation can be calculated based on following equation,

$$2b = 2a + 2H_d \tan^{-1} 54.74^\circ \quad (3.22)$$

Similarly, due to the symmetry of the SDCD, only one quarter of the whole structure is analyzed to reduce model complexity and computation time, as shown in Fig.3.11.

Also, appropriate boundary conditions for nodes on the clamped edge and symmetric boundary conditions for the nodes lying along the boundary between modeled and non-modeled regions are both imposed properly before simulations. The characteristic parameters of the composite diaphragm for FE simulation are listed in Table 3.2. Likewise, initial stresses are induced in the diaphragm by using the “cooling temperature” method as proposed by Zhang and Wise (Y. Zhang *et al.*, 1994). And stress stiffening effects can be included in ANSYS analysis.

Fig.3.11 ANSYS modeling of one quarter edge-clamped SDCD

3.3.4 Warpage

Warpage is a phenomenon whereby a static deflection occurs prior to any external loading, sometimes referred to as zero-pressure offset. This spontaneous deflection is different from buckling, which is a spontaneous deformation due to a very high compressive stress. When warpage becomes too large and even sometimes unstable, the

diaphragm would usually produce an unpredictable performance since its receiving area is no longer flat and symmetrical. For a planar diaphragm, its internal tensile stress is in-plane, which gives rise to in-plane forces. As a result, this internal tensile stress increases the rigidity of the planar diaphragm.

For a non-planar diaphragm, however, the internal tensile stress on the diaphragm is non-uniform. In this case, the resulting forces do not remain in-plane due to non-uniform stress distribution and the existence out-of-plane forces generate bending moments which cause the diaphragm to deflect or to “warp”. Using ANSYS, warpage behavior of an SDCCD under initial stress of 70MPa and free of external loads has been simulated. The deformed shape together with the original edges prior to deformation, for an SDCCD, are shown in Fig.3.12.

Fig.3.12 The warpage behavior of an SDCCD under an initial stress of 70MPa

The magnitude of warpage is a function of both the diaphragm stress levels and the diaphragm geometry. The higher the internal stress level, the larger the warpage since a higher internal stress will induce a larger bending moment. Warpage of single deeply corrugated diaphragms with a bottom region of $100 \times 100 \mu m^2$ and different corrugation depths from $10 \mu m$ to $80 \mu m$ under different initial stresses of 20MPa, 40MPa, 60MPa and 80MPa are simulated using FEM and the results are shown in Fig.3.13.

Figure 3.13 Warpages of SDCDs versus corrugation depths under different initial stresses

From Fig.3.13 we can see that the higher the internal stress level, the larger the zero-pressure offset. An interesting phenomenon is that, for a specific initial stress level, a larger warpage can be observed at the extremes of the corrugation depth range with a minimum value in mid range. This is because the rigidities of sidewalls of smaller corrugation depth are relatively larger than those of larger corrugation depth. Thus, in the case of small corrugation depth, the relaxation of the diaphragm structure becomes more difficult when it is being released, which results in large warpage. On the other

hand, when corrugation depth is large enough, the SDCD structure will become flexible and vulnerable at high initial stress, which also results in large warpage. For the low tensile stress composite diaphragms in this study, the observed warpages are in the direction of bending away from the substrate. This observation is consistent with FE simulation results.

3.3.5 Correction factors of the SDCD

Based on Eq.(3.19), for an SDCD structure with bottom diaphragm of $100 \times 100 \mu m^2$, the correction factors $C_1(H_d)$ and $C_2(H_d)$ can be calculated using the simulated mechanical sensitivities of diaphragms with and without initial stress. It should be pointed out that for an SDCD, its mechanical sensitivity should be obtained by computing the difference between the centre deflections under zero and 1Pa pressure loads because of the existence of warpage. The correction factors are shown in Fig.3.14.

From Fig.3.14, we can see that the simulation results match our previous results of suppositions very well. Both the initial stress effect and diaphragm stiffness can be reduced simultaneously with the increase of corrugation depth. When corrugation depth is larger than $40 \mu m$, initial stress effect can be reduced by as much as 90% while diaphragm stiffness can be reduced by nearly 40%. Therefore, among these different types of diaphragms, SDCD is expected to have the highest mechanical sensitivity in sensing application when equal-sized diaphragms are considered.

Fig.3.14 Calculated correction factors of $C_1(H_d)$ and $C_2(H_d)$ for an SDCD

$(100 \times 100 \mu m^2)$ with different corrugation depths

3.4 Mechanical sensitivity comparison

Mechanical sensitivity is one of the most important characteristic indices for sensing application. In the previous sections, the load-deflection behavior of different types of sensing diaphragms has been discussed. It can be seen that the mechanical sensitivity of

a diaphragm is mainly determined by diaphragm stiffness and initial stress. A flat diaphragm with a rigid centre can increase diaphragm stiffness and a shallowly corrugated diaphragm can reduce the initial stress effect while increasing its diaphragm stiffness at the same time. Only SDCDs can reduce both diaphragm stiffness and initial stress effect. Therefore, an SDCD should have the highest mechanical sensitivity and a flat diaphragm with a rigid centre should possess the lowest mechanical sensitivity, for the same geometrical dimensions. Compared with flat diaphragms, the improvement in mechanical sensitivity of a shallowly corrugated diaphragm is severely limited when diaphragm size is relatively small, as shown in Fig.3.5(b). On the other hand, the improvement in mechanical sensitivity for a shallowly corrugated diaphragm can become evident when diaphragm size becomes large. However, when shallowly corrugated diaphragm is used as optical sensing structure, since the corrugation needs to be manufactured on the topmost layer of the deflecting composite diaphragm, the corrugation depth is normally limited to less than $1\ \mu\text{m}$. Therefore, even for high-sensitivity applications (large diaphragm size), the improvement in mechanical sensitivity for shallowly corrugated diaphragm is still quite limited, as shown in Fig.3.5(a), where corrugation depth of less than $1\ \mu\text{m}$ is used. Fig 3.15 shows the simulated results of mechanical sensitivities of four diaphragm designs ($100\times 100\ \mu\text{m}^2$). The flat diaphragm, the flat diaphragm with a rigid centre of $b/a = 0.3$ and the shallowly corrugated diaphragm with a corrugation depth of $1\ \mu\text{m}$ are denoted as Flat, FlatRC, Shallow, respectively. For diaphragms with single deep corrugations, they are denoted as H20 to H100 with the numbers indicating different corrugation depths.

H20 = Corrugation depth of $20\ \mu\text{m}$

H40 = Corrugation depth of 40 μm

H60 = Corrugation depth of 60 μm

H80 = Corrugation depth of 80 μm

H100 = Corrugation depth of 100 μm

Fig.3.15 Mechanical sensitivities of four diaphragm designs ($100\times100\mu m^2$) at different stress levels (0-200MPa)

From Fig.3.15 it can be clearly seen that the mechanical sensitivity for SDCD is evidently higher when compared with those of the other three designs. This could be further summarized in Table 3.3 in which mechanical sensitivities of the diaphragms under an initial stress of 70MPa and with same size of $100\times100\mu m^2$ are tabulated. The percentage increase in mechanical sensitivity with respect to flat diaphragm is above 200% for the diaphragm with a single deep corrugation of 40 μm corrugation depth

(H40). As we illustrated before, because of the limitation of shallow corrugation depth, the shallow corrugation effect on improving diaphragm mechanical sensitivity is rather limited. Therefore the results are encouraging as mechanical sensitivity could be increased significantly by using a novel SDCD when a shallowly corrugated diaphragm has been found to be rather incompetent.

Table 3.3 Mechanical sensitivities of different diaphragm designs at an initial stress of 70MPa ($100\times100\mu\text{m}^2$)

Diaphragm Design	Flat	FlatRC b/a=0.3	Shallow	Single deep corrugation				
				H20	H40	H60	H80	H100
S_m (nm/kPa)	1.58	1.33	1.75	2.76	3.26	3.50	3.60	3.65
Percentage with respect to flat diaphragm	100	84	111	175	206	222	228	231

3.5 Comparison of non-planar deflection behavior

When uniformly loaded and edge-clamped diaphragm is used for optical sensing, the non-planar deflection will cause signal degradation in optical response as “Signal-averaging effect” which will be discussed in detail in Chapter 5. Signal-averaging effect is referred to as a pressure-dependent degradation of optical response which arises because the negative intensity values can be cancelled out with the positive ones, due to the sinusoidal nature of the optical response reflected from or transmitted

through an optical microstructure, thus leading to an averaged optical response from different positions of the non-uniformly deflected diaphragm. For this reason, the actual detected optical response may be lower than the expected value from the optical microstructure with an idealized piston-like diaphragm and the degradation increases with an increasing degree of the deflecting diaphragm curvatures. Therefore, accurate models are essential and need to be established to study the non-planar deflection behavior of the uniformly loaded and edge-clamped sensing diaphragm for better understanding of the signal-averaging effect.

In 1951, Marcus method (C. C. Chang *et al.*, 1951) was first applied to solve the small-deflection problem of a square plane diaphragm. Then based on thin plate theory (S. Timoshenko, 1987), Z. Xiao (Z. Xiao *et al.*, 1997) and W. P. Eaton (W. P. Eaton *et al.*, 2000) presented an analytical solution for circular diaphragm deflection for pressure sensing applications. Other analytical methods, such as Ritz method and Hermite Polynomials (R. Steinmann *et al.*, 1995), had also been applied to the analysis of square planar diaphragm under small deflections. Besides these, D. Maier-Schneider (D. Maier-Schneider *et al.*, 1995) have presented a new analytical solution for large-deflection of square flat diaphragms. Although finite element models have the potential to be very accurate, analytical models are still desirable because they are simple and can provide adequate physical insight into the problem. Moreover, simple analytical solutions can be easily coupled or incorporated into the subsequent optical analysis for better interpreting the signal-averaging effect.

3.5.1 Verification of ANSYS simulation on non-planar pressurized deflection

In order to verify the validity of ANSYS simulation results on the non-planar pressurized deflection behavior, analytical expression of Marcus method has been applied to analyze the edge-clamped flat square diaphragm and the results are compared with FE simulation. By using Marcus method and followed the techniques presented by C. C. Chang (C. C. Chang *et al.*, 1952), non-planar deflection of a flat square diaphragm of $100 \times 100 \mu\text{m}^2$ with Young's modulus of 200GPa and an initial stress of 70MPa under pressure P can be derived as ,

$$w = \frac{K}{46.95} \{1 - 0.2729 \sin(2.034\xi) \sinh(2.356\xi) - 0.1162 \cos(2.034\xi) \cosh(2.356\xi)\} \\ \times \{1 - 0.2729 \sin(2.034\eta) \sinh(2.356\eta) - 0.1162 \cos(2.034\eta) \cosh(2.356\eta)\} \quad (3.23)$$

$$K = \frac{a^4 \cdot P}{D}, \quad \xi = \frac{x}{a}, \quad \eta = \frac{y}{a} \quad (3.24)$$

where a is the half length of the flat diaphragm and D is the flexural rigidity which can be expressed as:

$$D = \frac{Eh^3}{12(1-\nu^2)} \quad (3.25)$$

where E is the Young's modulus, h is the diaphragm thickness and ν is the Poisson's ratio.

Based on the Eqs.(3.23)~(3.25), non-planar deflection of a flat diaphragm under pressure of 1KPa has been calculated and compared with FEM analysis results as shown in Fig.3.16. From this figure, we can see that ANSYS results match very well with those from the Marcus analytical method. This demonstrates that the FEM simulation process is sufficiently accurate and can be used to analyze non-planar deflection behaviors of other types of diaphragms.

Fig.3.16 Non-planar deflection behavior simulated by Marcus analytical method and ANSYS for a flat square diaphragm

3.5.2 Non-planar pressurized deflection for different diaphragms

Due to the complexity involved in the modeling and analytical solution for other edge-clamped sensing diaphragms, such as flat diaphragm with rigid centre, shallowly corrugated diaphragm and SDCCD, there have not been any reported analytical models

for these types of diaphragm in open literatures. As a result, FEM modeling has to be used to analyze the non-planar deflection behaviors.

Different types of diaphragms with an assumed initial stress of 70MPa are studied using ANSYS under 1KPa pressure to investigate the non-planar deflections and the results are shown in Fig.3.17. Single deep corrugation depth is varied from 20 to 100 μm to investigate the effect of corrugation depth on diaphragm non-planar deflection properties. For flat diaphragm with rigid centre, b/a is assumed as 0.3 (referred to Fig.3.6). All diaphragm sizes are assumed to be $100 \times 100 \mu\text{m}^2$. In Fig.3.17, the abscissa delegates the multiples of the distance from the centre that vary from -1 to 1 and the y coordinate delegates the ratio of displacement with centre deflection throughout the diaphragm. It is obvious that the larger the ratio is, the more flat the diaphragm will be under the pressurized deflection. From Fig.3.17, one can see that flat diaphragm with rigid centre displays very small non-planarity under pressurized deflection and behaves like a piston-like diaphragm under external loads especially for the central part of the diaphragm. However, due to the low mechanical sensitivity and the complexity involved in MEMS fabrication, the application of flat diaphragms with rigid centres used as sensing diaphragms in optical sensing structure is very limited. On the other hand, it can be seen from Fig.3.17 that SDCDs can substantially reduce the non-planar deflection effect and can achieve similar or even better pressurized planarity than flat diaphragms with rigid centres when corrugation depth above 80 μm is considered. Compared with flat diaphragms and shallowly corrugated diaphragms, a reduction ratio of non-planar deflection as high as 10 and 7 can be achieved in the case when corrugation depth is 100 μm . It is because that the suspended sidewalls of an SDCD can produce stress-releasing and buffering effects which transform loads at the

outer region of the diaphragm into local deformations with a minimum impact on the bottom central diaphragm. Therefore, an SDCD can be virtually regarded as a piston-like diaphragm under pressurized deflection, to a large extent. This is very attractive since any improvement in the diaphragm flatness under pressure-loading will minimize signal averaging effect of the output optical response, leading to the best performance of the optical sensing structure.

Fig.3.17 Non-planar deflection behavior of three diaphragm designs with an assumed initial stress of 70MPa

3.6 Stress concentration of the SDCD

The elementary stress formulas used in the design of structural members are based on the members having a constant section or a section with gradual change of contour. Such conditions, however, are hardly ever attained throughout the highly stressed region of the actual machine parts or structural members. The presence of shoulders,

grooves, holes, keyways, threads, and so on, results in modifications of the simple stress distributions so that localized high stresses may occur. This localization of high stress is known as stress concentration, measured by the stress concentration factor. For an SDCD, the possible stress concentration at the corners is studied using ANSYS FEM simulations. In simulation the area around the corner is remodeled with a much finer mesh and the boundary conditions are interpolated based on the first model by using the cut-boundary interpolation capability (CBDOF) available in ANSYS post processing. Due to symmetry, the refined ANSYS model of a quarter sector of an SDCD is shown in Fig.3.18 and the original coarse model can be found in Fig.3.11.

Fig.3.18 Refined ANSYS modeling of an SDCD

Through simulations, stress values are obtained for the corners of an SDCD using different coarse and refined ANSYS models, which have demonstrated that stress concentration does not occur at the corners of a novel SDCD. It is because a corner with a sharp angle is more prone to stress concentration while the angle of the corner

formed between the free side wall and the bottom flat region of an SDCD is an obtuse angle of 125.26° .

3.7 Design and analysis of the proposed pressure sensor

3.7.1 Pressure sensor using SDCD

Based on previous discussions, among different types of diaphragms, SDCDs can provide the highest mechanical sensitivity and the most desirable planarity under pressurized deflection. Therefore, SDCDs are most suitable to be realized as sensing diaphragms for pressure sensing applications. The complete design of a pressure sensor with a single deep corrugation (backplate-diaphragm structure) is presented in Fig.3.19.

Fig.3.19 Diaphragm-backplate structure of the proposed pressure sensor (a) Isometric view (b) Backplate viewed from the bottom

From Fig.3.19, the proposed pressure sensor consists of an SDCCD, a silicon wafer as its substrate and a backplate at the bottom of the cavity. For pressure sensor with pressure to be measured by optical techniques, polysilicon layers or silicon nitride layers are deposited on the backplate to be used as a mirror. The sacrificial layer is deposited between the diaphragm and the silicon substrate so that subsequently the diaphragm can be released. Another function served by the sacrificial layer is that the resultant air gap layer, after the release of the diaphragm, is essential to form a Fabry-Perot microcavity to be used in the detection of external pressure loads.

3.7.2 Detailed design for the proposed pressure sensor

a. Sensing diaphragm

Ease of fabrication is one of the crucial design considerations in the MEMS design of a pressure sensor. Square diaphragm is chosen since it can be fabricated easily by anisotropic etching in silicon wafer (100) and surface micromachining. As illustrated before, silicon wafer with crystal orientation of (100) and with a square opening will be etched at an angle of 54.74° with respect to its horizontal axis as shown in Fig.3.20 when exposed to Potassium hydroxide solution (KOH).

Fig.3.20 Etching of silicon wafer (100) in KOH solution

Based on previous discussions, for polySilicon/ Si_xN_y /polySilicon composite sensing diaphragm, the Young's modulus and the initial stress are assumed to be 200GPa and 70MPa, respectively. The size of the sensing diaphragm is $100 \times 100 \mu m^2$ with a thickness of $1.5 \mu m$ and corrugation depth of $40 \mu m$ to achieve a desired wide pressure sensing range of about 1atm. Such a diaphragm can provide sufficiently high mechanical sensitivity and desirable planarity under pressurized deflection.

Table 3.4 tabulates the design parameters and the mechanical properties of the proposed polySilicon/ Si_xN_y /polySilicon diaphragm with a single deep corrugation used in the pressure sensor. The simulated mechanical sensitivity of the diaphragm with a corrugation depth of $40\ \mu m$ is 3.26nm/KPa with a resonant frequency of 1.932MHz and warpage of $0.04\ \mu m$.

Table 3.4 Design parameters and mechanical properties of the proposed SDCD used in pressure sensor

Design Parameters			Mechanical Properties		
Area (μm^2)	Thickness (μm)	Corrugation Depth (μm)	Mech. Sensitivity (nm/KPa)	Resonant Frquency (MHz)	Warpage (μm)
100×100	1.5	40	3.26	1.932	0.04

b. Backplate

According to Fig.3.19 (a), since the thickness of the deposited mirror layer is normally less then $1\ \mu m$, which is very thin compared with the thickness of the silicon backplate which is usually about $10\ \mu m$, the mirror layer can be neglected when mechanical properties of the backplate are analyzed. The material properties of the backplate used in ANSYS FE simulation are shown in Table 3.5.

Table 3.5 Material properties of backplate in FE simulation

Description	Backplate
Materials	Bulk silicon (100)
Thickness	10 μm
Thermal coefficient of Expansion, α	$2.3 \times 10^{-6} / ^\circ C$
Young's Modulus, E	200GPa
Poisson's ratio, ν	0.22
Density, ρ	2330 kg/m ³

Since the air gap thickness between the backplate and sensing diaphragm is about several microns for our proposed structure, the moving of entrapped air in the narrow gap leads to increased viscous damping as indicated by Z. Skvor(Z. Skvor, 1967), and heat-reduction effects as reported by G. Plantier(G. Plantier *et al.*, 1990). This may result in serious damping of the high-frequency response and the damping leads to the increased mechanical Johnson noise. Therefore, it is necessary to reduce the length of the narrow path by making holes in the backplate. It should be pointed out that the center portion of the backplate should not be perforated because over which a mirror layer is to be deposited and it is essential to keep the mirror as flat and intact as possible when optical techniques are used to detect the deflection of the sensing diaphragm. The simulation procedure of the backplate is quite similar to that of the sensing diaphragm. The ANSYS modeling of the proposed perforated backplate with 128 backplate holes is presented as shown in Fig.3.21.

Fig.3.21 ANSYS modeling of one quarter edge-clamped perforated backplate

The simulation results of the backplate with a thickness of $10\mu m$ for three sets of backplate holes (4, 32, 128) whose radius is $2\mu m$ are tabulated in Table 3.6. It can be observed from Table 3.6 that there is no significant difference among the simulated values for the resonant frequency of the backplate as the number of backplate holes changes.

Table 3.6 Simulation results of resonant frequencies of perforated backplates

NO of backplate holes	Resonant Freq (MHz)
4	15.75
32	15.56
128	15.17

c. Air gap thickness

The air gap thickness between the diaphragm and the backplate needs to be carefully designed in order to satisfy both mechanical and optical requirements. From mechanics point of view, a moderate value should be selected for the air gap distance since too small air gap distance will lead to a drastic increase in air-gap resistance which will heavily damp the frequency response of the sensing diaphragm. Also, from micromachining point of view, a small air gap is more difficult to be achieved by releasing sacrificial layer due to the well-known ‘stick’ problem. On the other hand, when intensity-modulated optical techniques instead of cumbersome fringe counting optical techniques are used, small cavity length is preferred for the proposed pressure sensor. Therefore, the air gap thickness should be comparable to the operating wavelength. Furthermore, in optical modeling, the air gap distance should be optimized to ensure that the non-pressure transmittances are at the minimal values, thereby enlarging and enhancing the whole monotonic sensing range. Considering all these requirements, the air gap thickness is firstly chosen to be around $2\ \mu\text{m}$ and then finalized by the optical optimization process as will be discussed in Chapter 4.

d. Overall sensitivity

When the proposed pressure sensor is integrated with optical fiber, the pressurized sensing diaphragm deflection can be detected by measuring the variations in the transmitted or the reflected optical intensities. Therefore, the overall sensitivity of the whole pressure sensor assembly can be calculated as,

$$S_{\text{overall}} = S_m S_o \quad (3.26)$$

where S_m is the mechanical sensitivity of the diaphragm and S_o is the optical sensitivity.

Normally, the relation between the transmitted and the reflected optical intensity variations due to diaphragm deflection is periodic and nonlinear. However, it is still possible to choose one monotonic operating range as the sensing range, which is about 1/4 of the laser beam wavelength. Also, the linearity can be maximized by optimizing the whole optical structure, such as the mirror layer thickness and the air gap distance, which will be discussed in Chapter 4 in some detail. Therefore, by denoting the output power of the laser diode as E and the laser beam wavelength as λ , the optical sensitivity can be defined as,

$$S_o = \frac{4 \delta T E}{\lambda} \quad (3.27)$$

where δT is the transmittance variation within the monotonic sensing range.

For a laser diode output power of 4mw, wavelength of 1550nm and δT of 0.3 for the optimal optical structure as will be discussed in Chapter 4, the calculated sensitivity of the whole pressure sensor is $10.1 \mu \text{W/kPa}$.

e. Dynamic response of pressure sensor

Although static pressure can be measured under steady-state or equilibrium conditions, most real-life applications require dynamic or changing pressure to be measured. Therefore, when time-varying pressures are to be measured, frequency response of the pressure sensor must be considered. It is well known that as a rough approximation, the

sensor resonant frequency should be above 10 times of the highest frequency component of the pressure signal in order to avoid signal distortion or attenuation in measurement. For the proposed pressure sensor as shown in Fig.3.19, since the resonant frequency of the sensing diaphragm can reach as high as 2MHz, the sensor can thereby measure the dynamic pressure signal with highest frequency component above 200kHz, which is supposed to be high enough for most practical applications.

The frequency response of the whole pressure sensor is dominated by the effect of air damping. To obtain a flat frequency response and to eliminate the effect of attenuation due to air damping, ventilation holes are opened on the backplate of the sensor. The over-damped problem of the pressure sensor can therefore be reduced or even eliminated by increasing the number or the size of the ventilation holes.

3.8 Summary

In this chapter, theoretical analyses are performed on the load-deflection behaviors of four different sensing diaphragm designs, namely, the flat diaphragm, the shallowly corrugated diaphragm, the flat diaphragm with rigid center and the proposed SDCD. It has been observed that the problem of high initial stress in the diaphragm can be successfully addressed by the introduction of a single deep corrugation. Unlike the shallow corrugation technique, the proposed single deep corrugation technique has an advantage of significantly reducing the initial stress of the diaphragm without any increase in its bending stiffness. In this way, highest sensitivity can be achieved when an SDCD is used as a sensing diaphragm.

Also, the non-planar behavior under pressurized deflection for different diaphragm designs is studied using FE commercial software ANSYS. From the FEA results, it has demonstrated that, compared with flat diaphragms and shallowly corrugated diaphragms, the reduction of non-planar deflection of an SDCD can reach as high as 10 times when practically realistic corrugation depths are assumed. This almost piston-like travel property is very attractive since any improvement in the pressure-loaded diaphragm flatness will minimize the signal averaging effect of the output optical response, leading to the best performance of the optical sensing structure.

The design of a pressure sensor using an SDCD for applications of large pressure measurement ranges is then presented in this chapter. Each part of the proposed sensor, such as the sensing diaphragm, the backplate and the air gap thickness, is elaborately discussed in detail. The calculated overall sensitivity of the pressure sensor is $10.1 \mu W/kPa$ and the sensing range of differential pressure is about 1atm. For the proposed pressure sensor, it can be applied to measure dynamic pressure signals with a highest frequency component of 200kHz, which is sufficiently high for any practical applications.

CHAPTER 4

Novel Optical Modeling and Optimization of Fabry-Perot Microcavity for Sensing Applications

4.1 Introduction

Fabry-Perot microcavity based structures have been widely used for sensing applications, such as in the measurement of pressure and temperature, owing to their high measurement sensitivity and immunity to electromagnetic interference (EMI). This kind of sensor detects the variation in optical path length induced by either a change in the refractive index or a change in the physical length of microcavity. In this chapter, a generalized study has been carried out on the modeling of a Fabry-Perot microcavity for sensing applications. Different analytical models on transmission characteristics of Fabry-Perot microcavity are firstly established using plane wave based techniques, such as Macleod characteristic matrix technique (H. A. Macleod, 1976), transfer matrix technique (O. S. Heavens, 1965) and Smith's technique (S. D. Smith, 1958). A novel Gaussian optics based model for Fabry-Perot microcavity illuminated by laser beam is then developed and validated. The influence of laser beam waist on microcavity optical response is investigated and the required minimal beam waist size is explored to ensure useful optical response for sensing application which can be accurately predicted based on plane wave optics.

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It is well known that the performance of Fabry-Perot microcavity is greatly influenced by random film thickness variations (H. A. Macleod, 1976; P. Bousquet *et al.*, 1972; J. J. Monzon *et al.*, 1991). During fabrication and test, we also found that the sensor performance is heavily influenced by the process-induced mirror layer thickness variations, which are critically dependent on the fabrication process parameters such as etching time and release process. Even though the techniques of thickness control for thin films have been rapidly improved recently in practice, thickness variation is still very much a problem. Therefore Fabry-Perot microcavity should be optimized to achieve targeted performance stability for batch fabrication. In this chapter, the measurement error tolerance induced by manufacturing variations in Fabry-Perot mirror thicknesses is also investigated. A novel optimization model is then established to achieve both the preferable sensing performance and the least measurement error tolerance for required high yields during batch fabrication. The proposed optimized Fabry-Perot microcavity for sensing application is presented and the perturbation of microcavity performance induced by other common factors, such as non-normal incidence angle and microcavity mirror imperfections is further investigated.

4.2 Traditional plane wave based models for Fabry-Perot microcavity

For many years, the n -layer problem in optics had been considered to be intractable, until 1937, Rouard showed a method for closed form matrix solution in which multilayer structures can be analyzed by representing each layer as a 2×2 matrix (A. Nussbaum *et al.*, 1976; F. Abeles, 1967). Subsequent treatments have resulted in two different, but equivalent, formulations of Rouard's original method. One is the Macleod characteristic matrix technique (H. A. Macleod, 1976) which was developed from the basic Maxwell's equations. The other is the transfer matrix technique (O. S. Heavens,

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1965) using matrix containing the Fresnel coefficients and the phase propagation across the layers. In 1958, Smith's method (S. D. Smith, 1958), known as the method of effective interfaces was presented which can isolate any layer or any combination of several consecutive layers and examine their influences on the performance of the whole thin film assembly. All these distinct techniques rely upon a superposition of plane waves and will be utilized to analyze the optical response of Fabry-Perot microcavity for sensing applications. To give a general view for the most common case, the established models can treat both cases of normal incidence and non-normal incidence.

Fig.4.1 Schematic diagram of Fabry-Perot microcavity for sensing applications

Normally, the Fabry-Perot microcavity for sensing application is made up of multilayered stationary and moving mirrors as well as changeable cavity gap layer which is determined by physical quantity to be measured and in this case, the external

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loads as shown in Fig 4.1. In the following modeling, the illuminated beam is assumed to be transverse electric (S-Polarization) and the operated wavelength is assumed to be high enough so that all dielectric mirror layers and substrate are considered to be transparent and free of optical absorption. Therefore, the following discussions will focus on the modeling for the transmittance T of the Fabry-Perot microcavity since the corresponding reflectance R can be easily calculated by $R = 1 - T$.

4.2.1 Macleod characteristic matrix technique

According to Macleod (H. A. Macleod, 2001), for a plane wave incident on a thin film as shown in Fig.4.2, the tangential components of electric field strength E and magnetic field strength H at the interface a and b can be related in matrix notation as:

$$\begin{bmatrix} E_a \\ H_a \end{bmatrix} = \begin{bmatrix} \cos \delta & i \sin \delta / \eta_1 \\ i \eta_1 \sin \delta & \cos \delta \end{bmatrix} \begin{bmatrix} E_b \\ H_b \end{bmatrix} \quad (4.1)$$

Fig.4.2 Plane wave incident on a thin film (H. A. Macleod, 2001)

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In Eq.(4.1), η_1 is the optical admittance of thin film, i is the complex notation and δ is expressed as,

$$\delta = \frac{2\pi n_1 d \cos \theta_1}{\lambda} \quad (4.2)$$

n_1, d, θ_1 are refractive index, thickness and refractive angle of thin film layer, respectively and λ is the wavelength,

The 2×2 matrix on the right-hand side of Eq.(4.1) is known as the optical characteristic matrix of the thin film. Eq.(4.1) can be normalized by dividing E_b to both sides and rewritten as:

$$\begin{bmatrix} E_a / E_b \\ H_a / E_b \end{bmatrix} = \begin{bmatrix} B \\ C \end{bmatrix} = \begin{bmatrix} \cos \delta & i \sin \delta / \eta_1 \\ i \eta_1 \sin \delta & \cos \delta \end{bmatrix} \begin{bmatrix} 1 \\ \eta_s \end{bmatrix} \quad (4.3)$$

where η_s is the optical admittance of the substrate and $\begin{bmatrix} B \\ C \end{bmatrix}$ is known as the optical characteristic matrix of the assembly.

Upon application of this theory, the Macleod characteristic matrix of our proposed Fabry-Perot microcavity as shown in Fig.4.1 can then be described as,

$$\begin{bmatrix} B \\ C \end{bmatrix} = \left(\prod_{i=1}^q \begin{bmatrix} \cos \delta_i & \frac{i}{\eta_i} \sin \delta_i \\ i \eta_i \sin \delta_i & \cos \delta_i \end{bmatrix} \right) \begin{bmatrix} 1 \\ \eta_s \end{bmatrix} \quad (4.4)$$

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where $\delta_i = \frac{2\pi n_i d_i \cos \theta_i}{\lambda}$, n_i , η_i , d_i , θ_i are the refractive index, the optical admittance, the physical thickness and the angle of incidence angle of the i th thin film layer in the thin film assembly respectively, while η_s is the optical admittance of the substrate. Optical admittances of the film layer and the substrate are related as $\eta_i = n_i \eta_{fs} \cos \theta_i$, $\eta_s = n_s \eta_{fs} \cos \theta_s$, respectively, where η_{fs} is the optical admittance of the free space whose value is set to be $\eta_{fs} = \frac{1}{377}$, and θ_i, θ_s can be derived from θ_0 based on Snell's law,

$$n_0 \sin \theta_0 = n_i \sin \theta_i = n_s \sin \theta_s \quad (4.5)$$

Defining $Y \equiv C/B$, the reflectance R and the transmittance T of the thin film assembly can then be calculated as,

$$R = \left(\frac{\eta_0 - Y}{\eta_0 + Y} \right) \left(\frac{\eta_0 - Y}{\eta_0 + Y} \right)^* = \left(\frac{\eta_0 B - C}{\eta_0 B + C} \right) \left(\frac{\eta_0 B - C}{\eta_0 B + C} \right)^* \quad (4.6)$$

$$T = \frac{4\eta_0 \operatorname{Re}(\eta_m)}{(\eta_0 B + C)(\eta_0 B + C)^*} \quad (4.7)$$

where η_0 is the optical admittance of the incident medium and “*” denotes the complex conjugate operation.

4.2.2 Transfer matrix technique

The transfer matrix technique explicitly uses the Fresnel coefficients along with the superposition of electric fields to solve the problem of multilayer optics (O. S. Heavens, 1965).

Fig.4.3 shows a thin layer of thickness d , where a and b denote the two material interfaces of the thin film. An electric field of E_{0a}^+ is incident upon the thin film structure. The incident field causes a reflected field of E_{0a}^- , which travels in the opposite direction to the incident field and the internal transmitted field to the layer of E_{1a}^+ . The “+” and “-” signs indicate the directions of the travelling wave. The field transmitted through the film layer is E_{2b}^+ . It is assumed that E_{2b}^- is equal to zero, which practically means that there is no optical source or additional material boundaries which could cause reflections in the substrate.

Fig.4.3 Schematic view of a single thin film layer

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The aim of the transfer matrix method is to find the ratio of the reflected field E_{0a}^- and transmitted field E_{2b}^+ to the incident field E_{0a}^+ . A full description of the wave propagates through whole assembly can be written in matrix form as:

$$\begin{bmatrix} E_{0a}^+ \\ E_{0a}^- \end{bmatrix} = \frac{1}{\tau_{01}} \begin{bmatrix} 1 & \rho_{01} \\ \rho_{01} & 1 \end{bmatrix} \begin{bmatrix} e^{i\delta} & 0 \\ 0 & e^{-i\delta} \end{bmatrix} \frac{1}{\tau_{12}} \begin{bmatrix} 1 & 0 \\ r_{12} & 0 \end{bmatrix} \begin{bmatrix} E_{2b}^+ \\ 0 \end{bmatrix} \quad (4.8)$$

where τ , ρ delegate the amplitude transmission coefficient and the amplitude reflection coefficient of the interface, respectively.

Therefore, to describe the propagation characteristic of each film layer, one matrix is used for the intermediate interface and an additional matrix is used to account for the change in phase as the wave travels the thickness of each layer. Therefore, assumed there is no reflection in the exit substrate, for Fabry-Perot microcavity, a full description of the wave travelling through the thin film assembly can then be expressed as,

$$\begin{bmatrix} E_0^+ \\ E_0^- \end{bmatrix} = \left(\prod_{i=0}^{q-1} \frac{1}{\tau_{(i)(i+1)}} \begin{bmatrix} 1 & \rho_{(i)(i+1)} \\ \rho_{(i)(i+1)} & 1 \end{bmatrix} \begin{bmatrix} e^{i\delta_i} & 0 \\ 0 & -e^{i\delta_i} \end{bmatrix} \right) \frac{1}{\tau_{(q)(s)}} \begin{bmatrix} 1 & 0 \\ \rho_{(q)(s)} & 0 \end{bmatrix} \begin{bmatrix} E_s^+ \\ 0 \end{bmatrix} \quad (4.9)$$

where “+” and “-” signs indicate the travelling directions of the wave, $\tau_{(i)(i+1)}$, $\rho_{(i)(i+1)}$, and $\tau_{(q)(s)}$, $\rho_{(q)(s)}$ are the transmission and reflection coefficients of the interface between the i th and the $(i+1)$ th layer and the interface between the last q th layer and the substrate, respectively, which can be calculated according to Fresnel laws,

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$$\rho_{(i)(i+1)} = \frac{n_i \cos \theta_i - n_{i+1} \cos \theta_{i+1}}{n_i \cos \theta_i + n_{i+1} \cos \theta_{i+1}}; \quad \tau_{(i)(i+1)} = \frac{2 n_i \cos \theta_i}{n_i \cos \theta_i + n_{i+1} \cos \theta_{i+1}} \quad (4.10)$$

where $\theta_0, \theta_i, \theta_{i+1}, \theta_s$ are related through Snell's law,

$$n_0 \sin \theta_0 = n_i \sin \theta_i = n_{i+1} \sin \theta_{i+1} = n_s \sin \theta_s \quad (4.11)$$

Define a matrix t as,

$$t \equiv \begin{bmatrix} t_{11} & t_{12} \\ t_{21} & t_{22} \end{bmatrix} = \left(\prod_{i=0}^{q-1} \frac{1}{\tau_{(i)(i+1)}} \begin{bmatrix} 1 & \rho_{(i)(i+1)} \\ \rho_{(i)(i+1)} & 1 \end{bmatrix} \begin{bmatrix} e^{i\delta_i} & 0 \\ 0 & -e^{i\delta_i} \end{bmatrix} \right) \frac{1}{\tau_{(q)(s)}} \begin{bmatrix} 1 & 0 \\ \rho_{(q)(s)} & 0 \end{bmatrix} \quad (4.12)$$

Then, the ratio of the transmitted field to the incident field can be found by,

$$\frac{E_s^+}{E_0^+} = \frac{1}{t_{11}} \quad (4.13)$$

And the power transmission can be calculated as,

$$T = \frac{n_s \cos \theta_s}{n_0 \cos \theta_0} \left(\frac{1}{t_{11}} \right)^* \left(\frac{1}{t_{11}} \right) \quad (4.14)$$

For Eq.(4.14), one important thing needs to be pointed out is that $\cos \theta_s / \cos \theta_0$ should be added for non-normal incidence. Otherwise, the resultant analytical expressions will not coincide with the one resulted from Macleod transfer matrix technique.

4.2.3 Smith's technique

Compared with Macleod characteristic matrix technique and the transfer matrix technique, Smith's method can be applied to any layer in the thin film assembly and multiple reflections within each layer can be considered. The usefulness of this method is mainly in providing the possibility to isolate one layer or a combination of several layers and to examine their influence which these layers and any changes in these layers have on the performance of the whole thin film assembly. For the proposed Fabry-Perot microcavity, the cavity gap layer is therefore isolated to investigate the optical response variations induced by the applied external loads to be detected. The effective reflection and transmission coefficients at its boundaries are the resultant coefficients of multilayered moving mirror and multilayered stationary (fixed) mirror on either side which are denoted as ρ_m, τ_m and ρ_f, τ_f , respectively.

Since for a single film, the equivalent reflection coefficient ρ and transmission coefficient τ are (S. D. Smith, 1958),

$$\rho = \frac{\rho_1 + \rho_2 e^{-2i\delta}}{1 + \rho_1 \rho_2 e^{-2i\delta}} ; \quad \tau = \frac{\tau_1 \tau_2 e^{-i\delta}}{1 + \rho_1 \rho_2 e^{-2i\delta}} \quad (4.15)$$

where δ is the phase shift experienced by the wave as it transverses the film and can be calculated as:

$$\delta = \frac{2\pi n_1 d \cos \theta_1}{\lambda} \quad (4.16)$$

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where ρ_1, ρ_2, τ_1 and τ_2 are the reflection and transmission coefficients for the two consecutive interfaces 1 and 2 of the thin film which can be calculated according to Fresnel laws of Eq.(4.10). The complex coefficients ρ_m, τ_m, ρ_f and τ_f can then be calculated by applying Eq.(4.15) successively, first to the last two interfaces and to replace them by a single interface with the resultant coefficients, and then to this equivalent interface and the third last interface, and so on. Let,

$$\rho_m = |\rho_m| e^{i\varphi_{\rho m}}; \tau_m = |\tau_m| e^{i\varphi_{\tau m}}; \rho_f = |\rho_f| e^{i\varphi_{\rho f}}; \tau_f = |\tau_f| e^{i\varphi_{\tau f}} \quad (4.17)$$

The resultant transmittance of the whole Fabry-Perot microcavity can be presented as (H. A. Macleod, 2001):

$$T = \frac{n_s \cos \theta_s}{n_0 \cos \theta_0} \frac{|\tau_m|^2 |\tau_f|^2}{\left(1 - |\rho_m| |\rho_f|\right)^2 + 4 |\rho_m| |\rho_f| \sin^2 \left(\frac{\varphi_{\rho m} + \varphi_{\rho f}}{2} - \delta \right)} \quad (4.18)$$

where $\delta = 2\pi n_g g \cos \theta_g / \lambda$, g is the cavity gap distance and n_g, θ_g are the refractive index and refractive angle of cavity gap layer, respectively. According to Snell's law,

$$n_0 \sin \theta_0 = n_g \sin \theta_g = n_s \sin \theta_s \quad (4.19)$$

4.3 Novel Gaussian optics modeling for Fabry-Perot microcavity

4.3.1 Motivation for developing a different model

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Since Fabry-Perot microcavity are commonly used in conjunction with laser beams especially in sensing applications, the difference between laser beam propagation features and those of a plane wave can lead to a considerable difference in the resultant optical response. It is well established that a laser beam is Hermite-Gaussian in general and is Gaussian in its fundamental mode TEM_{00} (H. Kogelnik *et al.*, 1966; J. T. Verdeyn, 1989). Therefore, when a laser beam is used, the characteristics of a Fabry-Perot microcavity could not be simply evaluated by summing the plane waves obtained by multiple reflection from the interferometer surfaces. The passage of laser beam through a Fabry-Perot cavity has been discussed in just a few papers and different theoretical approaches have been followed to derive an expression for the intensity of beam emerging from the cavity. It was first studied by summing the successive transmitted Gaussian beams of decreasing intensities (H. Abu-Safia *et al.*, 1994; Z. M. Wu *et al.*, 2003). Another method described the incident Gaussian beam by its plane-wave Fourier expansions and then calculated the corresponding spatial and spectral responses of a Fabry-Perot interferometer (F. Moreno *et al.*, 1992; E. Nichelatti *et al.*, 1995). However, these methods were applicable only to Fabry-Perot microcavities with mirrors of single layer and even so, they could not provide explicit analytical expressions. Explicit analytical expressions of transmission characteristics of Gaussian beams through a thin transparent plate were presented by Cotteverte and Poirson (J. C. Cotteverte *et al.*, 1991; J. Poirson *et al.*, 1995) and in our work, such analytical approach is further extended to study the Fabry-Perot microcavity with multilayered mirrors. The overall optical transmission characteristic model of a Fabry-Perot microcavity with multilayered mirrors has been established, for the first time, using Gaussian beam optics in conjunction with Smith's method.

4.3.2 Novel Gaussian optics based model

Consider a case where a Gaussian beam of size w and curvature radius R is incident with an angle θ_0 on a Fabry-Perot microcavity with multilayered thin film mirrors. Also, assume that the air gap depth, the refractive indices of the incident medium (air), the cavity gap layer (air) and the exit medium (substrate) are g, n_0, n_g and n_s , respectively. Here, the influence of the shift of Gouy phase $\zeta(z)$ is negligible because the microcavity film thickness is very small when compared with the Rayleigh range z_0 ($z_0 = \pi \omega_0^2 / \lambda$, ω_0 is the laser beam waist size) and therefore Gouy phase shift does not evolve significantly within the cavity. Also, since the travel distance of Gaussian beam in microcavity is much smaller than Rayleigh range, the size w and the curvature radius R_b of the Gaussian beam are considered to be unchanged.

For Gaussian beam, the transversal spatial dependence of the electric field associated with the incident beam can be written as (J. C. Cotteverte *et al.*, 1991)

$$E_i(x, y) = A \exp\left(-\frac{x^2 + y^2}{\omega^2}\right) \exp\left[\frac{i\pi}{\lambda R_b}(x^2 + y^2)\right] \quad (4.20)$$

In order to focus on the transmission characteristics and to simplify the analysis of Gaussian beam through Fabry-Perot microcavity, Fabry-Perot microcavity is treated as one single cavity with two equivalent interfaces as shown in Fig.4.4. In this mathematical model, the equivalent reflection and transmission coefficients ρ_m, τ_m for the moving mirror interface and ρ_f, τ_f for the stationary mirror interface are calculated by using Smith's method, respectively, as discussed in section 4.2.3.

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Fig.4.4 Simplified mathematical model used in Gaussian optics based modeling

Referring to Fig.4.4, the electric field associated with the successive transmitted beams through cavity gap can be written as,

$$E_t(x, y) = \sum_{p=0}^{\infty} B_p \exp(-i(p + 1/2)\psi') \exp\left(-\frac{(x - p\Delta x)^2 + y^2}{\omega^2}\right) \exp\left[\frac{j\pi}{\lambda R_b}((x - p\Delta x)^2 + y^2)\right] \quad (4.21)$$

where

$$B_p = \tau_m \tau_f (\rho_m \rho_f)^p A \quad (4.22)$$

is the intensity of the p th transmitted beam,

$$\psi' = 4\pi n_g g \cos \theta_g / \lambda \quad (4.23)$$

is the Gaussian beam phase advance for one round trip in the air gap, and

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$$\Delta x = 2g \tan \theta_g \cos \theta_0 \quad (4.24)$$

is the transversal walkoff between successively transmitted beams.

Coefficients ρ_m, τ_m, ρ_f and τ_f are complex and can be written as,

$$\rho_m = |\rho_m| e^{i\varphi_{\rho_m}}; \tau_m = |\tau_m| e^{i\varphi_{\tau_m}}; \rho_f = |\rho_f| e^{i\varphi_{\rho_f}}; \tau_f = |\tau_f| e^{i\varphi_{\tau_f}} \quad (4.25)$$

Upon substitution of Eq.(4.22) and Eq.(4.25) into Eq.(4.21), one yields,

$$E_t(x, y) = \sum_{p=0}^{\infty} \tau \rho^p A \exp(i(\varphi + p\psi)) \exp\left(-\frac{(x - p\Delta x)^2 + y^2}{\omega^2}\right) \exp\left[\frac{i\pi}{\lambda R_b}((x - p\Delta x)^2 + y^2)\right] \quad (4.26)$$

where,

$$\varphi = \varphi_{\tau_m} + \varphi_{\tau_f} - \psi' / 2; \psi = \varphi_{\rho_m} + \varphi_{\rho_f} - \psi'; \tau = |\tau_m| |\tau_f|; \rho = |\rho_m| |\rho_f| \quad (4.27)$$

Therefore, we can calculate the transmittance T of the whole Fabry-Perot microcavity as,

$$T = \frac{n_s \cos \theta_s}{n_0 \cos \theta_0} \frac{\int_{-\infty}^{\infty} dy \int_{-\infty}^{\infty} dx |E_t(x, y)|^2}{\int_{-\infty}^{\infty} dy \int_{-\infty}^{\infty} dx |E_i(x, y)|^2} \quad (4.28)$$

According to Snell's law,

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$$n_0 \sin \theta_0 = n_g \sin \theta_g = n_s \sin \theta_s \quad (4.29)$$

Following the analytical approach presented by Poirson (J. Poirson *et al.*, 1995), let,

$$N_p \equiv \sum_{p=0}^{\infty} \tau \rho^p A \exp(-i(\varphi + p\psi)) \exp\left(-\frac{(x - p\Delta x)^2 + y^2}{\omega^2}\right) \exp\left[\frac{i\pi}{\lambda R_b}((x - p\Delta x)^2 + y^2)\right] \quad (4.30)$$

then,

$$|E_t(x, y)|^2 = \sum_{p=0}^{\infty} \sum_{q=0}^{\infty} N_p N_q^* \quad (4.31)$$

By grouping all the terms of the same order $u = p + q$ and separating the cases where u is even from where u is odd, one has,

$$|E_t(x, y)|^2 = \sum_{k=0}^{\infty} \left(\sum_{v=0}^{2k} N_{2k-v} N_v^* + \sum_{v=0}^{2k+1} N_{2k+1-v} N_v^* \right) \quad (4.32)$$

After pairing all the complex-conjugate terms,

$$|E_t(x, y)|^2 = \sum_{k=0}^{\infty} \left(N_k N_k^* + \sum_{p=1}^k (N_{k-p} N_{k+p}^* + N_{k+p} N_{k-p}^*) + \sum_{p=0}^k (N_{k+1+p} N_{k-p}^* + N_{k-p} N_{k+1+p}^*) \right) \quad (4.33)$$

On the other hand, from Eq.(4.33),

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$$N_k N_k^* = \tau^2 \rho^{2p} A^2 \exp\left(-\frac{2((x-p\Delta x)^2 + y^2)}{\omega^2}\right) \quad (4.34)$$

$$\begin{aligned} & N_{k-p} N_{k+p}^* + N_{k+p} N_{k-p}^* \\ &= 2\tau^2 \rho^{2k} A^2 \exp\left(-\frac{(x-(k-p)\Delta x)^2 + (x-(k+p)\Delta x)^2 + 2y^2}{\omega^2}\right) \\ &\times \cos\left(-2p\psi + \pi((x-(k-p)\Delta x)^2 - (x-(k+p)\Delta x)^2) / \lambda\rho\right) \end{aligned} \quad (4.35)$$

$$\begin{aligned} & N_{k+1+p} N_{k-p}^* + N_{k-p} N_{k+1+p}^* \\ &= 2\tau^2 \rho^{2k+1} A^2 \exp\left(-\frac{(x-(k+1+p)\Delta x)^2 + (x-(k-p)\Delta x)^2 + 2y^2}{\omega^2}\right) \\ &\times \cos\left(-(2p+1)\psi + \pi((x-(k-p)\Delta x)^2 - (x-(k+1+p)\Delta x)^2) / \lambda\rho\right) \end{aligned} \quad (4.36)$$

Using the results given by Gradshteyn (I. S. Gradshteyn *et al.*, 1968),

$$\int_{-\infty}^{\infty} \exp(-ax^2) \cos(2bx) dx = \sqrt{\frac{\pi}{a}} \exp\left(-\frac{b^2}{a}\right) \quad (4.37)$$

$$\int_{-\infty}^{\infty} \exp(-ax^2) \sin(2bx) dx = 0 \quad (4.38)$$

$$\int_{-\infty}^{\infty} \int_{-\infty}^{\infty} \exp\left(-2\frac{x^2 + y^2}{\omega^2}\right) dx dy = \omega^2 \frac{\pi}{2} \quad (4.39)$$

one can have,

$$\int_{-\infty}^{\infty} \int_{-\infty}^{\infty} N_k N_k^* dx dy = \tau^2 \rho^{2k} A^2 \omega^2 \frac{\pi}{2} \quad (4.40)$$

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$$\begin{aligned} & \int_{-\infty}^{\infty} \int_{-\infty}^{\infty} (N_{k-p} N_{k+p}^* + N_{k+p} N_{k-p}^*) dx dy \\ &= 2\tau^2 \rho^{2k} A^2 \cos(2p\psi) \exp\left(-\frac{(2p)^2 \Delta x^2}{2} \left[\frac{1}{\omega^2} + \left(\frac{\pi\omega}{\rho\lambda}\right)^2\right]\right) \end{aligned} \quad (4.41)$$

$$\begin{aligned} & \int_{-\infty}^{\infty} \int_{-\infty}^{\infty} (N_{k+1+p} N_{k-p}^* + N_{k+1+p} N_{k-p}^*) dx dy \\ &= 2\tau^2 \rho^{2k+1} A^2 \cos((2p+1)\psi) \exp\left(-\frac{(2p+1)^2 \Delta x^2}{2} \left[\frac{1}{\omega^2} + \left(\frac{\pi\omega}{\rho\lambda}\right)^2\right]\right) \end{aligned} \quad (4.42)$$

Since for laser beam,

$$\frac{1}{\omega_0^2} = \frac{1}{\omega^2} + \left(\frac{\pi\omega}{\rho\lambda}\right)^2 \quad (4.43)$$

where ω_0 is the beam waist size of the Gaussian beam. From Eqs.(4.33), (4.40)-(4.43), the transmittance T for the whole Fabry-Perot microcavity in Eq.(4.28) can be simplified and rewritten as,

$$\begin{aligned} T = & \frac{n_s \cos \theta_s}{n_0 \cos \theta_0} \tau^2 \sum_{k=0}^{\infty} \rho^{2k} \left(1 + 2 \sum_{p=1}^k \cos(2p\psi) \exp\left[-\frac{(2p)^2 \Delta x^2}{2\omega_0^2}\right] \right. \\ & \left. + 2\rho \sum_{p=0}^k \cos((2p+1)\psi) \exp\left[-\frac{(2p+1)^2 \Delta x^2}{2\omega_0^2}\right] \right) \end{aligned} \quad (4.44)$$

4.3.3 Validation of novel optical model

For our proposed Fabry-Perot microcavity based sensing element as shown in Fig.4.5, the moving mirror is formed by one silicon nitride layer sandwiched between two polysilicon layers ($polySi/Si_3N_4/polySi$) and the stationary mirror comprises two layers of polysilicon layer and silicon nitride layer ($polySi/Si_3N_4$). Since a moderately low finesse FP microcavity suffices for sensing application (W. J. Wang *et al.*, 2004), the thickness of each layer in the composite diaphragm can be chosen to mainly satisfy mechanical constraints such as low tensile residual stress and suitable mechanical stiffness. The operating wavelength can be selected as 1300nm or 1550nm so that silicon substrate becomes highly transparent to such a wavelength and wavelength of 1550nm will be used in the following simulations. The simulated sensing range of applied external pressure is determined by ensuring the corresponding deflection of the moving mirror is larger than one half of the operating wavelength.

Fig.4.5 Proposed Fabry-Perot microcavity structure

The microcavity transmittance is firstly simulated using both the novel Gaussian optics based technique and the plane wave based techniques under different incidence angles of $0^\circ, 20^\circ, 40^\circ, 50^\circ$, respectively, as shown in Fig.4.6. Gaussian beam waist ω_0 of 1mm is used in the simulation.

Fig.4.6 Transmittance comparisons of different plane wave based models and novel Gaussian optics based model on a Fabry-Perot microcavity under different incidence angles (a) 0°, (b) 20°, (c) 40°, (d) 50°

From Fig.4.6, it can be seen that consistency of results from different plane wave based optical models is demonstrated through the excellent agreements between the Macleod characteristic matrix technique, the transfer matrix technique and the Smith's

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technique. Also, the proposed novel Gaussian beam based optical model is successfully validated through the agreements of simulation results between the Gaussian beam based technique and the plane wave based technique at both normal incidence and small angle incidence. Meanwhile, small deviations can be observed when incidence angle becomes large enough. This can be explained that, with the increase of incidence angle, the transversal walkoff $\Delta x = 2d \sin \theta_0$ also increases (for air cavity gap layer, $\vartheta_g = \theta_0$ for Eq.(4.50)) and the successive transmitted beams will be spatially distributed. The overlapping between the beams will therefore become less which weakens the interference among them and results in a smaller transmittance.

4.3.4 Influence of beam waist on the optical response of Fabry-Perot microcavity

For Fabry-Perot microcavity illuminated by laser beam, the shift of Gouy phase $\zeta(z)$ and the variation in Gaussian beam size w and curvature radius R are negligible. Therefore the influence of beam waist on the resultant transmittance will be investigated. Figure 4.7 shows the simulated transmittance of Fabry-Perot microcavity without external loads and illuminated by laser beam with different ratio of Gaussian beam waist size to cavity length ranging from 0.1 to 100. Fig.4.7 shows that, when the beam waist is comparable to the cavity length, pronounced transmittance variations are observed for non-normal incidences which will result in the severe deviation in Fabry-Perot optical response compared with the ones predicted by plane-wave techniques. On the other hand, when the beam waist is large enough compared with the cavity length, the wavefront will behave very closely to a plane wave and the simulated transmittance can be observed to saturate to a stable value. Also, transmittance fluctuations go up

with the increase of the incidence angle and no transmittance fluctuation can be observed under normal incidence since the transversal walkoff Δx become zero.

Fig.4.7 Transmittance fluctuations of an unpressurized Fabry-Perot microcavity illuminated by laser beam with different ratios of Gaussian beam waist size to cavity length ranging from 0.1 to 100 under different incidence angles

The influence of the beam waist size to Fabry-Perot microcavity sensing performance under different incidence angles is further investigated as shown in Fig.4.8. As predicted, it can be seen that for a very small angle of incidence, transmittance deviations due to beam waist size is negligible and optical responses are in good agreements with those simulated by plane wave based techniques. However, with the increase of the incidence angle, the deviation becomes much more severe especially for the laser beam with small beam waist size. When the incidence angle increases to 50° , even the transmittance of laser beam with large beam waist of $\omega_0/g = 10$ is evidently different from that of the plane wave and the Fabry-Perot microcavity illuminated by laser beam with beam waist of $\omega_0/g = 0.5$ and $\omega_0/g = 1$ can hardly be used as

pressure sensing element. Therefore, it is preferable to select laser beam waist size at least 10 times more than the cavity length so that the optical performance of the Fabry-Perot microcavity illuminated by laser beam can be accurately predicted by plane wave based optics and is applicable for sensing applications under both normal and nonnormal incidences.

Fig.4.8 The influence of laser beam waist on resultant Fabry-Perot transmittance under different incidence angles (a) 0° , (b) 20° , (c) 40° , (d) 50°

4.4 Optimization of Fabry-Perot microcavity for sensing applications

4.4.1 Modeling of measurement error induced by manufacturing variations

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For the Fabry-Perot pressure sensor, the applied external loads can be detected by the variations of the measurand, such as the transmittance or the reflectance of the Fabry-Perot microcavity. For the ease of the illustrations, the following models will focus on the Fabry-Perot microcavity of transmittance detection scheme, while the Fabry-Perot microcavity of reflectance detection scheme can be similarly modeled. For a q -layer Fabry-Perot microcavity as shown in Fig.4.9, layers 1 through $k-1$ make up the moving mirror, layer k is the cavity gap to be measured and layers $k+1$ through q make up the stationary mirror. The thickness of i th layer is d_i and the corresponding manufacturing-induced thickness variation is Δd_i . In practice, the Fabry-Perot microcavity with ideal nominal thicknesses is normally used in the calibration of the applied external loads. Therefore, the induced measurement error can be evaluated from two different structures, one with layer thicknesses all equal to their nominal design values ($d_1, d_2, \dots, g, \dots, d_n$) and induced measurement error of cavity gap Δg_{err} , and another with layer thicknesses ($d_1 + \Delta d_1, d_2 + \Delta d_2, \dots, g + \Delta g_i, \dots, d_n + \Delta d_n$), to produce the same measurand, such as transmittance T , i.e.,

$$T(d_1, d_2, \dots, g + \Delta g_{err}, \dots, d_n) = T(d_1 + \Delta d_1, d_2 + \Delta d_2, \dots, g + \Delta g_i, \dots, d_n + \Delta d_n) \quad (4.45)$$

where Δg_i is the manufacturing variation of initial gap distance. It is critical to remember that the Fabry-Perot microcavity is also formed by the deposition and release of the sacrificial layer so that Δg_i still needs to be considered.

Fig.4.9 Schematic view of a general q -layer Fabry-Perot microcavity for sensing application

For both sides of Eq.(4.45), a first-order Taylor series approximation can be performed,

$$\begin{aligned}
 & T(d_1, d_2, \dots, g, \dots, d_n) + \Delta g_{err} \left(\frac{\partial T}{\partial g} \right) \bigg|_{d_1, d_2, \dots, g, \dots, d_n} \\
 & = T(d_1, d_2, \dots, g, \dots, d_n) + \sum_{i=1}^q \Delta d_i \left(\frac{\partial T}{\partial d_i} \right) \bigg|_{d_1, d_2, \dots, g, \dots, d_n} + \Delta g_i \left(\frac{\partial T}{\partial g} \right) \bigg|_{d_1, d_2, \dots, g, \dots, d_n}
 \end{aligned} \tag{4.46}$$

Therefore,

$$\Delta g_{err} = \Delta g_i + \left[\left(\frac{\partial T}{\partial g} \right) \bigg|_{d_1, d_2, \dots, g, \dots, d_n} \right]^{-1} \sum_{i=1}^q \Delta d_i \left(\frac{\partial T}{\partial d_i} \right) \bigg|_{d_1, d_2, \dots, g, \dots, d_n} \tag{4.47}$$

Considering that the cavity layer thickness varies within ε_i percent, the possible maximal error Δg_{errm} can be expressed as (D. G. Guo *et al.*, 2004),

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$$\Delta g_{errm} = \varepsilon_l \left(g_i + \left[\left(\frac{\partial T}{\partial g} \right) \right]_{d_1, d_2, \dots, g, \dots, d_n}^{-1} \sum_{i=1}^q d_i \left| \left(\frac{\partial T}{\partial d_i} \right) \right|_{d_1, d_2, \dots, g, \dots, d_n} \right) \quad (4.48)$$

The transmittance T is derived from novel Gaussian optics based model as Eq.(4.48), which has already discussed in Section 4.3. Since it is usually impossible to obtain exact analytical expressions of $\partial T / \partial g$ and $\partial T / \partial d_i$, a common practical approach is to find a high-order fitting function to relate the transmittance T to g or d_i and such a fitted function can ensure the necessary accuracy of $\partial T / \partial g$ and $\partial T / \partial d_i$ over a required range of application. From Eq.(4.48), it can be seen that Δg_{errm} is a function of the cavity gap distance g . Since the variation in cavity gap is linearly dependent on the applied external load p , if we define the nominal initial cavity gap distance as g_i and the maximum mechanical travel of the moving mirror over the full range of stimuli as s , the worst-case measurement accuracy ε_m can then be defined as,

$$\varepsilon_m = \max \left\{ \frac{\Delta p_{err}}{p} \right\} = \max \left\{ \frac{\Delta g_{errm}}{g} : g \in [g_i, g_i + s] \right\} \quad (4.49)$$

In terms of yield, we assume the fraction of devices with i th layer thicknesses between $d_i - \Delta d_i$ and $d_i + \Delta d_i$ are P_i and the fraction of devices with initial cavity gap distances between $g_i - \Delta g_i$ and $g_i + \Delta g_i$ are P_g . According to the theory of probability (E. T. Jaynes, 2003), the micromachining processes for mirror layers and air gap layer can be regarded as the independent events since whatever happens to one won't affect another. Therefore, the total possibility P_s , which means the fraction of

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sensors with measurement accuracy not worse than ε_m , can be established by multiplying all the probabilities of these independent events as,

$$P_s = P_g \prod_{i=1}^n P_i \quad (4.50)$$

4.4.2 Modeling of Fabry-Perot microcavity optimization for sensing applications

If Fabry-Perot pressure sensors are to be manufactured in volume at low cost, it will not be practical to calibrate each sensor individually to eliminate process-induced response variations. Therefore, the microcavity should be optimized to achieve high yield while maintaining reasonable accuracy. Moreover, from the viewpoint of sensing applications, it is also critical to achieve the best sensing performance, such as the least percent nonlinearity error and the preferable sensing resolution. For unoptimized Fabry-Perot microcavity as shown in Fig 4.5, high mirror reflectance will result in sharp peak and small variations over some parts of the sensing range, while low mirror reflectance will result in small variations within the whole sensing range, both of which are not suitable for sensing applications. Therefore, the percent nonlinearity error then becomes a key parameter in the optimization to ensure the required gradual variation of optical response within the whole monotonic sensing range. Since the period of the Fabry-Perot microcavity transmittance is $\lambda/2$ and contains two basic operating monotonic branches, the maximal monotonic range is selected as $\lambda/4$ and the corresponding maximal applied external loads can be determined by mechanical compliance of the moving mirror. As one of the most common cases to be encountered in practice, the following discussions consider the incident laser beam as being normal to Fabry-Perot microcavity.

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For this multi-objective optimization problem, we define a vector x as $x = (d_1, d_2, \dots, d_k, g, d_{k+1}, \dots, d_n)$ to represent the Fabry-Perot microcavity, where $d_1, \dots, d_k$ are the moving mirror layer thicknesses, g is the initial gap distance, $d_{k+1}, \dots, d_n$ are the fixed mirror layer thicknesses, as shown in Fig.4.9. The objectives of the optimization can then be written as,

$$(1) \min\{\varepsilon_m(x)\}$$

$$(2) \min\{\varepsilon_{nl}(x)\}$$

where ε_m is the worst-case measurement accuracy which can be derived from Eq.(4.49), ε_{nl} is the percentage nonlinearity of the measurand (i.e. transmittance) to the applied external loads which can be expressed as,

$$\varepsilon_{nl}(x) = \max \left\{ \frac{T(g) - T_L(g)}{T_L(g)}, g \in [g_i, g_i + s] \right\} \quad (4.51)$$

where $T(g)$ is the transmittance under different external loads and $T_L(g)$ is the linear least square interpolation of the transmittance.

Using weighted summing strategy, the original optimization can be converted into a single scalar optimization problem by constructing an evaluation objective parameter ε_{obj} ,

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$$\varepsilon_{obj}(x) = \max \left\{ \frac{T(g) - T_L(g)}{T_L(g)} + \frac{\Delta g_{errm}}{g} : g \in [g_i, g_i + s] \right\} \quad (4.52)$$

The problem can then be solved using a standard constrained optimization algorithm.

The boundary conditions of this optimization process are listed as follows,

- (1) To achieve the preferable measurement resolution, the transmittance variation $\Delta T(g)$ over a monotonic range should be made as large as possible,

$$\Delta T(g) \geq \delta_{T(g)} \quad (4.53)$$

where $\delta_{T(g)}$ is regarded as an optimization parameter.

- (2) The thickness of each layer in the composite thin film assembly need to be chosen to mainly satisfy mechanical constraints such as low tensile residual stress and suitable mechanical stiffness, therefore, the ratio of different component mirror layer thicknesses need to be carefully controlled.

- (3) For easy micromachining, the designed thickness of each mirror layer in the composite thin film assembly is chosen as the multiple of 10nm.

- (4) In order to maximize the measurement range, it is desirable to adjust the microcavity transmittance to the smallest value when no external loads are applied,

$$T(g) \geq T(g_i), \quad g \in [g_i, g_i + s] \quad (4.54)$$

(5) The percentage nonlinearity error ε_{nl} should be controlled to be within a limited value ε_v . In practice, ε_v is normally set to 10% to ensure the validity of the optimization.

$$\varepsilon_{nl} \leq \varepsilon_v \quad (4.55)$$

4.4.3 Optimization of the proposed Fabry-Perot microcavity

For the proposed Fabry-Perot microcavity, the moving mirror is formed by one silicon nitride layer sandwiched between two polysilicon layers ($polySi / Si_3N_4 / polySi$) and the fixed mirror is formed by one polysilicon layer combined with one silicon nitride layer ($polySi / Si_3N_4$), as shown in Fig.4.5. The ratio between polysilicon layer thickness and silicon nitride layer thickness needs to be controlled within a range of 1 to 2 to ensure that the final resultant equivalent stress of the thin film stack be small but positive so that the fabricated composite film stack is slightly under tension. In the simulation, the cavity gap distance variation per psi is based on the result of mechanical analysis from Chapter 3.

Based on Eqs.(4.48)~(4.55), the optimization is firstly performed for the Fabry-Perot pressure sensor using transmittance detection scheme and under operating wavelength of 1550nm. During the optimization, we found that for this type of sensor, the maximum value of the optimization parameter $\delta_{T(g)}$ can only be chosen as 0.3 to ensure that practical optimization results meaningful can be obtainable. Under this theoretical limitation, the resultant Fabry-Perot microcavity structures which satisfy maximal percentage nonlinearity errors ε_{nl} less than 5% within the whole sensing

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range are listed in Table 4.1, where 1 denotes the moving mirror and 2 denotes the stationary mirror. The cavity gap distances shown in Table 4.1 are optimized for each Fabry-Perot microcavity so that the non-pressure transmittance is at its minimal value, thereby enlarging the whole monotonic sensing range. The optimal sensing range is also tuned to the monotonic range of optical response, which is about 1atm.

Table 4.1 Fabry-Perot microcavity structures which satisfy maximum percentage nonlinearity errors ε_{nl} less than 5% within the whole sensing range

	<i>Polysilicon</i> ¹ (NM)	<i>Si_xN_y</i> ¹ (NM)	<i>Polysilicon</i> ¹ (NM)	CAVITY GAP (NM)	<i>Polysilicon</i> ² (NM)	<i>Si_xN_y</i> ² (NM)
1	470	520	510	1650	410	290
2	490	420	590	2180	220	140
3	500	410	590	2110	220	150
4	510	490	400	2060	410	260
5	570	450	480	1900	210	180
6	600	410	490	1960	410	260
7	600	430	470	2020	230	210

The simulated transmittances with applied external loads for these optimized Fabry-Perot microcavities are shown in Fig.4.10. It can be seen from the figure that the optical responses of these optimal microcavities are similar and the transmittances all vary from about 0.7 to 1. However, although these Fabry-Perot microcavities have similar optical responses and all satisfy the linearity requirements, their worst-case measurement

accuracy ε_m are quite different. Therefore, further optimization is needed to establish which one has the smallest ε_m within the whole sensing range.

Fig.4.10 Transmittances under different external loads of all the Fabry-Perot microcavities listed in Table 4.1

Before the simulation of worst-case measurement accuracy, it should be pointed out that for Eq.(4.48), since it is usually impossible to obtain exact analytical expressions of $\partial T / \partial g$ and $\partial T / \partial d_i$, a practical common approach is to find a high-order fitting function to approximate the transmittance T which is a function of g and d_i , and from this approximate transmittance T , computer $\partial T / \partial g$ and $\partial T / \partial d_i$ with necessary accuracy over a required range of application. Therefore, the simulation results will be determined by the order of polynomial fitting function and the least order should be established to ensure simulation accuracy. Fig.4.11 shows the simulated worst-case measurement accuracy of an optimal Fabry-Perot microcavity (the 7th in table 4.1), where different order polynomial fitting functions are used in the simulation.

Fig.4.11 The worst-case measurement accuracy of an Fabry-Perot microcavity (the 7th shown in table 4.1) simulated using different order polynomial fitting functions

From Fig.4.11, we can see that a low order polynomial fitting function results in a big deviation and the responses will converge to accurate values with the increase of the order. Also, it can be seen that an order of 12 is high enough to ensure the required accuracy and will be used in the subsequent simulations. The evaluation objective parameter ε_{obj} with the air gap distance, the percent nonlinearity error ε_{nl} and the worst-case accuracy ε_m of these Fabry-Perot microcavities, as listed in Table 4.1, are simulated and shown in Fig.4.12(a)-(g).

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Fig.4.12 Simulated worst-case measurement accuracies of all Fabry-Perot microcavities as listed in Table 4.1

Although discernable differences can be observed from Fig.4.12 between different Fabry-Perot microcavities, they do have some common features. For all the cases studied, the worst-case accuracy decreases with the increase of applied external loads until a peak occurs, then it decreases again. Two relatively higher worst-case accuracies happen under no pressure and a pressure of 14~15psi. It can be explained that for these two pressure ranges, the transmittance T approaches to an extremum which can be inferred from Fig 4.10. Therefore $\partial T / \partial g$ takes the smallest values which are approaching zero and ε_m will become the largest in the nearby region according to Eq.(4.48). The maximal worst-case accuracies for each Fabry-Perot microcavity, as listed in Table 4.1, are about 22%, 9%, 28%, 118%, 17%, 48%, 7.5%, respectively. Therefore, only the 2nd and the 7th Fabry-Perot microcavities shown in Table 4.1 are suitable to be realized as Fabry-Perot pressure sensors and the 7th is selected as the final optimal structure in our design because of its smallest worst-case accuracy.

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For the optimal microcavity structure, the thickness combination of moving mirror is 600nm/430nm/470nm, while that of the fixed mirror is 230nm/210nm and the initial cavity gap distance is 2020nm as shown in Fig.4.13.

Fig 4.13 Optimal Fabry-Perot microcavity structure for transmittance detection scheme and under operating wavelength of 1550nm

The simulated transmittance and its linear interpolation with the applied external loads for this optimal Fabry-Perot microcavity are shown in Fig.4.14(a). The residual stress of sensing diaphragm is assumed to be 80MPa in the simulation. It should be pointed out that the linearly interpolated values are used only to evaluate the linearity of Fabry-Perot optical response, while the proposed Fabry-Perot pressure sensors will still be calibrated according to simulated values. Also, the resultant evaluation objective parameter ε_{obj} , the contribution of percentage nonlinearity error ε_{nl} and the worst-case accuracy ε_m are shown in Fig 4.14(b).

Fig.4.14 (a) Transmittance and its linear least square interpolation, (b) Evaluation object parameter ε_{obj} , percent nonlinearity error ε_{nl} and worst-case accuracy ε_m in the monotonic measurement range, of the optimal Fabry-Perot microcavity using transmittance detection scheme and under operating wavelength of 1550nm

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From Fig 4.14, it can be seen that the maximal transmittance percentage nonlinearity and the worst-case accuracy are less than 4% and 8% within the whole measurement range, respectively. Meanwhile, in terms of yield, if the fraction of devices with i th layer thickness between $d_i - \Delta d_i$ and $d_i + \Delta d_i$ and the fraction of devices with the initial air gap distance between $g_i - \Delta g_i$ and $g_i + \Delta g_i$ are both 99% ($P_i = P_g = 0.99$), the satisfied fraction of sensors P should be $[0.99]^6$, i.e., at least 94%.

Similarly, the optimization processes are further performed for Fabry-Perot sensors using transmittance detection scheme under operating wavelength of 1300nm as well as reflectance detection scheme under operating wavelengths of 1300nm and 1550nm. The residual stress of sensing diaphragm is assumed to be 80MPa in all simulations. It should be pointed out that for these three cases, the permissible maximal percentage nonlinearity errors ε_{nl} within the whole sensing range is increased from 5% to 6% to ensure that practical optimization results meaningful can be obtainable. The corresponding optimal Fabry-Perot microcavity structures and their optical response as well as the resultant evaluation object parameter ε_{obj} within the monotonic sensing range are simulated and the results are shown in Fig.4.15 - 4.18.

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Fig.4.15 Optimal Fabry-Perot microcavities for transmittance and reflectance detection schemes and under different operating wavelengths

Fig.4.16 (a) Transmittance and its linear least square interpolation, (b) Evaluation object parameter ε_{obj} and its components within the monotonic measurement range, of the optimal Fabry-Perot microcavity (refer to Fig.4.15(a)) using transmittance detection scheme and under operating wavelength of 1300nm

Fig.4.17 (a) Reflectance and its linear least square interpolation, (b) Evaluation object parameter ε_{obj} and its components within the monotonic measurement range, of the optimal Fabry-Perot microcavity (refer to Fig.4.15(b)) using reflectance detection scheme and under operating wavelength of 1300nm

Fig.4.18 (a) Reflectance and its linear least square interpolation, (b) Evaluation object parameter ε_{obj} and its components within the monotonic measurement range, of the optimal Fabry-Perot microcavity (refer to Fig.4.15(c)) using reflectance detection scheme and under operating wavelength of 1550nm

4.5 Modeling of microcavity performance perturbations

In this section, we will further investigate the perturbation of Fabry-Perot microcavity response caused by other common factors, such as the non-normal incidence and the imperfections of microcavity mirror. Two Fabry-Perot microcavity structures using transmittance detection scheme will be simulated and compared throughout this section. One is the optimal structure shown in Fig.4.13 and the other is an unoptimized structure shown in Fig.4.5.

4.5.1 Perturbation induced by non-normal incidence angle

In reality, it is unpractical to assume an absolutely normal incidence for a Fabry-Perot microcavity because of the possible errors induced during packaging or improper use. Therefore, the resultant absolute percentage transmittance fluctuations of the optimal and unoptimized Fabry-Perot microcavities induced by different small incidence angles are simulated and the results are shown in Fig 4.19.

It can be seen from Fig 4.19 that the transmittance perturbations for an optimal microcavity can be well controlled within 1% which is only about one third of those of an unoptimized mirrocavity.

Fig 4.19 Transmittance perturbations induced by non-normal incidence angles up to 5° for Fabry-Perot microcavities (a) Optimal (b) Unoptimized

4.5.2 Modeling of perturbations due to imperfections of micromirrors

A practical Fabry-Perot microcavity’s performance is influenced and limited by the presence of imperfections of mirror. In reality, high quality flat diaphragms tend to have a residual spherical curvature in most of the cases, or sometimes, a microscopic polishing defects or a combination of both. The influence of symmetric and asymmetric surface defects on optical response of Fabry-Perot cavity has been extensively investigated and one common approach for modeling defects is by projecting all surface defects onto one surface with the holding mirror is assumed to be perfectly flat. Therefore, a Fabry-Perot microcavity in the practice can be regarded as being made up of a large number of ideal elementary Fabry-Perot microcavities with different cavity

lengths. The final intensity distribution will be the superposition of all the intensities from all these ideal elementary Fabry-Perot microcavities.

Normally, mirror defects can be classified into three categories such as non-parallelism, initial residual spherical curvature and mirror imperfection (roughness) (P. D. Atherton *et al.*, 1981), as illustrated separately in Fig.4.20.

Fig.4.20 Schematic view of mirror imperfections

Non-parallelism defect and initial residual spherical curvature defect are symmetric defects and are easy to be taken into consideration in the transmittance calculation of elementary ideal Fabry-Perot microcavities. According to Eq.(4.44), let $\hat{T}$ represents the modified transmittance of a Fabry-Perot microcavity with surface defects, ψ' the phase advance corresponding to a double passage of light within the Fabry-Perot microcavity of nominal cavity length and ψ'_{ei} the perturbation or cavity length induced error of ψ' associated with the i th ideal elementary microcavity, the modified transmittance can then be calculated as,

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$$\hat{T} = \frac{\sum_{i=1}^n T(\psi' + \psi'_{ei}) A_{mi}}{A_m} \quad (4.56)$$

where A_{mi} is the area of the i th infinitesimally divided mirror section and A_m is the area of whole mirror. Similarly, the resultant absolute percentage transmittance fluctuations of the optimal and unoptimized Fabry-Perot microcavities induced by initial residual spherical curvatures up to 50nm and non-parallelisms up to 50nm within the whole sensing range and under normal incidence are simulated and the results are shown in Fig.4.21 and Fig.4.22, respectively.

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Fig.4.21 Transmittance perturbations of the optimal Fabry-Perot microcavity induced by (a) initial residual spherical curvatures up to 50nm (b) non-parallelisms up to 50nm

Fig.4.22 Transmittance perturbations of the unoptimized Fabry-Perot microcavity induced by (a) initial residual spherical curvatures up to 50nm (b) non-parallelisms up to 50nm

Gaussian distribution is considered as a good approximation for many micromachining processes, such as LPCVD (E. T. Carlen *et al.*, 1998). However, for the performance perturbation induced by surface roughness which can be assumed to be a Gaussian distribution, this type of defect is too complicated to be directly incorporated into elementary transmittance calculation. Therefore it is critical to develop a theoretical approach for the evaluation of this most common case. Let $P(h_r)$ be the function which describes the Gaussian distribution of defects over the mirror surface,

$$P(h_r) = \frac{1}{\sigma\sqrt{2\pi}} e^{-h_r^2/2\sigma_r^2} \quad (4.57)$$

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where h_r represents the height of the irregularities on an ideal equivalent surface of Fabry-Perot microcavity and with a mean value of zero, while σ_r is the root-mean-square (RMS) value of the surface roughness. The modified intensity distribution in the presence of the surface imperfections is then obtained by convolution of Eq.(4.44) and Eq.(4.57) (G. S. Bhatnagar *et al.*, 1974),

$$\begin{aligned}
 T(d) &= \int_{-\infty}^{\infty} T(d - h_r) P(h_r) dh_r \\
 &= \frac{n_s \cos \theta_s \tau^2}{n_0 \cos \theta_0 \sigma_r \sqrt{2\pi}} \sum_{k=0}^{\infty} \rho^{2k} \int_{-\infty}^{\infty} \left\{ 1 + 2 \sum_{p=1}^k e^{-\frac{(2p)^2 \Delta x^2}{2 \omega_0^2}} \cos \left(2p \psi' - \frac{8\pi p \cos \theta_g}{\lambda} h_r \right) \right. \\
 &\quad \left. + 2 \sum_{p=1}^k e^{-\frac{(2p+1)^2 \Delta x^2}{2 \omega_0^2}} \cos \left((2p+1) \psi' - \frac{4\pi(2p+1) \cos \theta_g}{\lambda} h_r \right) \right\} e^{-\frac{h_r^2}{2\sigma_r^2}} dh_r
 \end{aligned}
 \tag{4.58}$$

We make use of the following standard integrals needed to solve the above equation,

$$\int_0^{\infty} e^{-px^2} \sin(qx) dx = \sum_{l=1}^{\infty} \frac{(-1)^{l+1} l!}{(2l)!} \frac{q^{2l-1}}{p^l}
 \tag{4.59}$$

$$\int_0^{\infty} e^{-px^2} \cos(qx) dx = \frac{e^{-q^2/4p}}{2} \sqrt{\frac{\pi}{p}}
 \tag{4.60}$$

$$\int_0^{\infty} e^{-px^2} dx = \frac{1}{2} \sqrt{\frac{\pi}{p}}
 \tag{4.61}$$

Eq.(4.58) can then be simplified to become,

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$$T(d) = \frac{n_s \cos \theta_s}{n_0 \cos \theta_0} \tau^2 \sum_{k=0}^{\infty} \rho^{2k} \left\{ 1 + \frac{4}{\sigma_r \sqrt{2\pi}} \sum_{p=1}^k e^{-\frac{(2p)^2 \Delta x^2}{2\omega_0^2}} (A_1 \cos(2p\psi') + B_1 \sin(2p\psi')) \right. \\ \left. + \frac{4\rho}{\sigma_r \sqrt{2\pi}} \sum_{p=1}^k e^{-\frac{(2p+1)^2 \Delta x^2}{2\omega_0^2}} [A_2 \cos(2p+1)\psi' + B_2 \sin(2p+1)\psi'] \right\} \quad (4.62)$$

where,

$$A_1 = \frac{1}{2} e^{-(8\pi p \sigma_r \cos \theta_g / \lambda)^2 / 2}, B_1 = \sum_{l=1}^{\infty} \frac{(-1)^{l+1} l!}{(2l)!} (8\pi p \cos \theta_g / \lambda)^{2l-1} (2\sigma_r^2)^l \quad (4.63)$$

$$A_2 = \frac{1}{2} e^{-(4\pi(2p+1) \sigma_r \cos \theta_g / \lambda)^2 / 2}, B_2 = \sum_{l=1}^{\infty} \frac{(-1)^{l+1} l!}{(2l)!} (4\pi(2p+1) \cos \theta_g / \lambda)^{2l-1} (2\sigma_r^2)^l \quad (4.64)$$

The modified transmittances for the optimal Fabry-Perot microcavity shown in Fig.4.13 under normal incidence induced by root-mean-square (RMS) values of mirror surface roughnesses of 10nm, 20nm, 30nm are simulated and compared with ideal transmittance as shown in Fig.4.23.

Fig.4.23 Transmittance perturbations of Fabry-Perot microcavity induced by surface roughnesses of 10nm, 20nm and 30nm

It is clearly shown from Fig.4.23 that with the increase of RMS roughness value, the transmittance deviation also increases. The absolute percent transmittance fluctuations induced by RMS roughness values up to 10nm within the whole sensing range and under normal incidence for both optimal and unoptimized Fabry-Perot microcavities are further simulated and the results are shown in Fig.4.24.

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Fig.4.24 Transmittance perturbations of RMS surface roughnesses up to 10nm for different Fabry-Perot microcavities (a) Optimal (b) Unoptimized

From Fig.4.21 to Fig.4.24 it can be seen that for both the optimal and the unoptimized Fabry-Perot microcavities, since the perturbation induced by parallel deviation can be

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canceled out to some extent, its influence to the resultant transmittance is evidently smaller when compared with other two types of defects. Another interesting point can be observed is that the behaviour of the transmittance fluctuation induced by surface roughness is similar to that induced by initial residual spherical curvature. Such similarity can be inferred from not only the optimal microcavity as shown in Fig.4.24(a) and Fig.4.21(a), but also the unoptimized microcavity as shown in Fig.4.24(b) and Fig 4.22(a). Also, it can be seen that the optical response perturbations induced by non-normal incidence or mirror imperfections of the optimal Fabry-Perot microcavity are much smaller than that of the unoptimized microcavity. This suggests that the optimization not only minimizes the nonlinearity error and process-induced measurement error, but also greatly reduces the performance perturbations when non-ideal conditions are considered. For the optimal Fabry-Perot microcavity using transmittance detection scheme, when initial spherical curvature and RMS surface roughness are controlled within 10nm and 8nm, respectively, for a $100\mu\text{m} \times 100\mu\text{m}$ moving mirror, the induced transmittance variation can be brought to be within 1%.

4.6 Summary

The transmission characteristics of a Fabry-Perot microcavity have been studied using both plane wave based techniques such as Macleod characteristic matrix technique, the transfer matrix technique and Smith's technique, and the newly developed Gaussian beam based technique. A novel Gaussian optics based model for Fabry-Perot microcavity illuminated by laser beam has been developed and validated. The influence of laser beam waist on microcavity optical response has been investigated and it has been established to select laser beam waist size over 10 times more than the cavity

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length so that the optical performance of Fabry-Perot microcavity illuminated by laser beam can be accurately predicted by plane wave based optics theory. The technique developed is applicable for sensing applications under both normal and nonnormal incidences.

For Fabry-Perot microcavity used for sensing applications, a novel optimization model has been then established. Four optimal Fabry-Perot microcavities are obtained for both transmittance and reflectance detection schemes and under different operating wavelengths of 1300nm and 1550nm. The proposed optimal Fabry-Perot microcavities can constrain the percentage nonlinearity error within 6% and the worst-case measurement error to be within 10%, while at least 94% yield of sensors during batch fabrication can be ensured.

Furthermore, optical response perturbations induced by non-normal incidence as well as different types of microcavity mirror imperfections such as non-parallelism, initial residual spherical curvature and surface roughness, have been modeled. Simulation results have shown that proposed optimal Fabry-Perot microcavity displays better performance stability when compared with other unoptimized microcavities. For an optimal Fabry-Perot microcavity using transmittance detection scheme and under wavelength of 1550nm, when the non-normal incidence angle, the initial spherical curvature and the RMS surface roughness are controlled to be within 5° , 10nm and 8nm, respectively, the induced transmittance perturbations can be brought to be within 1%.

Chapter 5

Improvements and Characterizations of SDCCD Fabry-Perot Pressure Sensors

5.1 Introduction

Recent advances in silicon micromachining techniques have made optical sensing devices more feasible for commercialization by reducing cost and improving performance and manufacturability. Membrane-type Fabry-Perot microcavity pressure sensors have been fabricated using micromachining techniques (Y. M. Kim *et al.*, 1995; D. C. Abeysinghe *et al.*, 2001; J. Han *et al.*, 2000). However, there are several critical stumbling blocks facing the successful realization of this type of pressure sensors as discussed in Section 1.1.

One of the major obstacles is the degradation of device performance arising from the non-planar deflection of the edge-clamped diaphragm/mirror when it is subject to external pressure load, which is referred as signal-averaging effect. Following the discussions in Chapter 3, both the signal-averaging effect and the zero pressure effect will be elaborately modeled and compared in this chapter for different micromachined Fabry-Perot pressure sensor, i.e., the planar bottom diaphragm pressure sensors, the planar top diaphragm pressure sensor, the shallowly corrugated top diaphragm pressure sensor and the single deeply corrugated diaphragm (SDCCD) pressure sensor.

On the other hand, temperature dependence has always been a weakness of Fabry-Perot cavity-based pressure sensor with conventional flat diaphragm since stresses caused by the mismatch in coefficients of thermal expansions (CTEs) among materials fluctuate with ambient temperature and disturb the sensor signal. Since it is difficult to eliminate totally the temperature dependence of a Fabry-Perot pressure sensor just by selecting materials, minimizing the temperature dependence by designing a proper diaphragm structure is one of the most effective ways. It has been suggested that a corrugated diaphragm is capable of reducing the influence of packaging stress and thermally induced stress on a mechanical sensor (V. L. Spiering *et al.*, 1993; W. J. Wang *et al.*, 2003). Therefore, in this chapter, the effects of both the temperature variation and the thermally induced stress on the optical performance of the Fabry-Perot pressure sensor will be modeled and evaluated. A mathematical analysis will be presented to describe the stress reduction of the SDCD decoupling zone. The low thermal cross-sensitivity of an SDCD pressure sensor resulting from the significant stress-release effect will be further validated.

5.2 Schematic view of different Fabry-Perot pressure sensors

Optical metrology is an emerging promising area which has yet to be extensively commercialized for sensing applications. Of the various optical sensing techniques available, Fabry-Perot interferometry shows considerable promise in terms of its high sensitivity, versatility and immunity to environmental noises. Fabry-Perot based sensors have been broadly studied due to their capabilities for signal “amplification” (i.e., resonance) and tenability (J. P. Dakin *et al.*, 1987; J. Farah, 1995). Recently, Fabry-Perot optical pressure sensors have been introduced as an alternative to conventional piezoresistive and capacitive pressure sensors to eliminate signal

degradations in harsh environments. In addition, piezoresistive pressure sensors show non-linear sensitivity at elevated temperature and capacitive pressure sensors produce fairly small output capacitances, while a micromachined Fabry-Perot pressure sensor has been demonstrated to share none of these advantages. However, commercialization of Fabry-Perot pressure sensor is still in its early stage of maturity because of the non-resolved problems of signal averaging effect and thermal cross-sensitivity to temperature. Commercialization will remain to be remote until these problems are resolved.

5.2.1 Different deflecting diaphragm schemes

Normally, a typical Fabry-Perot microcavity based pressure sensor is interconnected with an optical fiber to transmit the incident and reflected or transmitted optical intensity, as shown in Fig.5.1. For one of the most common and practical cases, the optical fiber is supposed to be normal to the mirror surface of the Fabry-Perot sensor. Based on the location and the shape of a deflecting diaphragm, Fabry-Perot pressure sensors can be classified into four types. These are the planar bottom diaphragm pressure sensor, the planar top diaphragm pressure sensor, the shallowly corrugated top diaphragm pressure sensor and the SDCD bottom diaphragm pressure sensor as shown in Fig.5.1(a)-(d) respectively.

Fig.5.1 Schematic cross-sectional view of different Fabry-Perot pressure sensors (a) Planar bottom diaphragm, (b) Planar top diaphragm, (c) Shallowly corrugated top diaphragm, (d) SDCD bottom diaphragm

It should be noticed that it becomes very difficult to install optical fiber onto the diaphragm without causing any breakage when the lateral size of the bottom diaphragm is larger than the size of the optical fiber. Therefore, the anisotropically etched hole through the (100) silicon wafer is usually so sized so that the bottom diaphragm is smaller than that of the fiber and the walls of the pyramidal hole can serve as a mechanical stop as illustrated in Fig.5.1. It should be pointed out that the Fabry-Perot

sensor using a flat diaphragm with a rigid center discussed in Chapter 3 is not proposed here because of its practical complexity involved in micromachining process.

5.2.2 Optical sources and optical fibers

Generally, fiber optic sensors can be classified into intrinsic sensors and extrinsic sensors. Extrinsic sensors are those which use fibers to supply lights to sensing components and return signal lights to detection systems while intrinsic sensors use fibers themselves as transducer. Fabry-Perot pressure sensors presented in Fig.5.1 are therefore belong to those of the extrinsic fiber optic sensors. In the Fabry-Perot pressure sensors, the diaphragms are made of multilayered dielectric layers where the deflecting diaphragms serve as moving mirrors and the undeflected diaphragms serve as stationary mirrors.

In order to select an appropriate optical fiber and its corresponding optical source for the proposed Fabry-Perot pressure sensor, it is necessary to understand the various characteristics of different types of fiber. Usually, fiber optic communication systems operate in one of the 850-nm, the 1300-nm, and the 1550-nm wavelength windows. And semiconductor laser sources are then designed to operate at wavelengths that minimize optical fiber absorptions and maximize system bandwidths.

Single-mode fiber is best designed for longer transmission distances, making it suitable for long-distance telephony and multichannel television broadcast applications. The core size of a single mode fiber is small and is typically around 8 to 10 μm . A fiber core of this size allows only the fundamental or the lowest order mode to propagate and the most common operating wavelengths for single-mode optical fibers are 1300nm

and 1550nm. Single-mode fibers are capable of transferring higher amounts of data due to their low fiber dispersions. However, existing light-emitting diodes (LEDs) were unable to focus and launch sufficient optical power into single mode fibers for long-haul, very high-bandwidth communication systems. Therefore, single-mode fibers typically must use laser diodes as sources.

Multimode fiber, the first to be manufactured and commercialized, simply refers to the fact that numerous modes or light rays are carried simultaneously through the waveguide. Multimode fiber can provide high bandwidth over medium distances. Typical multimode fiber core diameters are 50, 62.5, and 100 micrometers. However, for step-index multimode fiber, multiple paths of light can cause signal distortion at the receiving end in long cable runs, resulting in an unclear and incomplete data transmission. Therefore, most present day applications use graded-index multimode fibers. And the multimode graded-index fiber with a core and cladding size of 62.5/125 μm offers the best combination of relatively high source-to-fiber coupling efficiency, low loss, low sensitivity to microbending and high bandwidth. Currently, the operating wavelengths for multimode graded-index fibers are 850nm and 1300nm.

For a multimode fiber, a large core size and a higher numerical aperture (NA) can have several advantages. The most important advantage is that multimode fibers permit the use of ordinary LEDs, while single-mode fibers typically must use laser diodes. LEDs are cheaper, less complex, and last longer, thereby becoming increasingly preferred for most applications. Another advantage is that light can be launched into a multimode fiber more easily. Also the higher value of NA and the larger core size make it easier to make fiber connections because core-to-core alignment becomes less critical during

fiber splicing. Multimode fiber nevertheless has its own disadvantages. As the number of modes increases, the effect of modal dispersion also increases. Therefore, multimode fiber is only suited for short and medium transmission distances.

For the Fabry-Perot pressure sensor, both single-mode fiber and multimode fiber can be employed. A single-mode fiber interconnected with the Fabry-Perot pressure sensor can be used for long-distance measurements, especially for those measurements under extremely harsh environments where accessibility is very poor. On the other hand, a multimode fiber interconnected with Fabry-Perot pressure sensor becomes attractive because of its low price and medium operating distance which is adequate for most applications. No matter what type of optical fiber is used, the illuminating wavelength should be high enough so that all dielectric mirror layers and the silicon substrate can be considered to be transparent and free of optical absorptions. Therefore, the operating wavelength of 850nm for the multimode fiber is abandoned since the silicon substrate and the polysilicon mirror layer become translucent under this wavelength. In a word, both single-mode fiber operating at 1300nm and 1550nm and multimode fiber operating at 1300nm can be interconnected with those Fabry-Perot pressure sensors shown in Fig.5.1.

Before proceeding following discussions, the concept of mode-field diameter needs to be introduced. Mode-field diameter (MFD) describes the size of the light-carrying portion of the fiber. In multimode fibers, all the light is confined within the core so that MFD is the core diameter as shown in Fig.5.2.(a). In single-mode fibers, however, this statement does not hold true. The essential portion of the light (typically, about 20%) is carried by the cladding and as a result, the MFD includes the fiber core as well as a small portion of the surrounding cladding glass. For single-mode fibers, MFD, rather

than core diameter, is the functional parameter that determines optical performance when a fiber is coupled to a light source. It is a function of wavelength, core diameter, and the refractive-index difference between the core and the cladding. Typical dimensions of MFD and the core diameters of step-index single-mode fibers are $9.3\ \mu\text{m}$ and $8.3\ \mu\text{m}$, respectively, as shown in Fig.5.2 (b).

Fig.5.2 Schematic view of MFD of (a) a multimode fiber and (b) a single-mode fiber

To highlight the influence of different mechanical deflecting diaphragm schemes on the performance and device characteristics of a Fabry-Perot microsensor, similar Fabry-Perot optical structure schemes should be used for comparisons. Referring to Fig.5.1, the applied pressure is detected by the variation of the reflected light intensity from the sensor. Therefore, using the optimization results established in Chapter 4, two optical structures, which are optimized for reflectance detection under operating wavelengths of 1300nm and 1550nm, will be used in the following discussions as shown in Fig.5.3. Because of the periodic properties of the detected optical response due to applied

pressure, the optimal monotonic sensing range is normally set to 1/4 of the operating wavelength.

Fig.5.3 Optimal Fabry-Perot optical structure schemes with wavelengths (a) 1300nm
(b) 1550nm

5.3 Signal averaging effect

In the previous simulations on optical responses of Fabry-Perot sensors, centre deflections of diaphragms are commonly used to calculate the air gap changes induced by applied pressures. However, since diaphragms with clamped boundaries have non-planar deflection properties, the actual measured optical intensities become quite different due to the non-uniform air gap distance and its resultant non-uniform optical response. Therefore, the actual optical response of a Fabry-Perot sensor needs to be averaged. When the applied pressure increases, the difference between the actual and the ideal optical responses also increases. This pressure-dependent degradation of the output optical response, which is caused by the variation in the amount of deflection

over the optical sensing area due to the bowing nature of the deflecting diaphragm under clamped boundary, is commonly referred as “signal averaging effect”.

5.3.1 Principle

In order to better understand the signal averaging effect, optical sensing area is introduced to facilitate the illustration. As shown in Figure 5.4, optical sensing area is the active area in a Fabry-Perot pressure sensor and is defined as the minimum transmitting area throughout the entire path of the optical beam from the optical fiber to the photo detector, via the cavity structure.

For the planar bottom diaphragm pressure sensor, Fig.5.4 shows that the difference in Fabry-Perot cavity gap spacing between the center position A and the edge position B of the optical sensing area becomes larger with the increasing deflection of the moving bottom diaphragm. Subsequently, the variation of the resultant reflected intensity as a function of the edge-to-center distance of the optical sensing area from B to A becomes greater. Since the sinusoidal nature of a Fabry-Perot cavity optical response causes possible cancellations of positive and negative reflected intensity values from different positions in the optical sensing area of a diaphragm, the detected reflected intensity with actual bowed diaphragm will be lower than its expected value with an ideal piston-like diaphragm (J. Han *et al.*, 2000). The degree of signal averaging effect is therefore determined by the degree of the curvature of a deflecting diaphragm within the optical sensing area.

Fig.5.4 Schematic view showing the “Optical sensing area” of the reflected intensity measurement

5.3.2 Nominal averaged reflectance

Since the signal averaging effect is caused by the non-uniform cavity gap distance throughout the optical sensing area, the actual optical response of a Fabry-Perot sensor needs to be averaged. Similar to the modeling of a Fabry-Perot microcavity performance perturbation discussed in Chapter 4, the stationary mirror is assumed to be perfectly flat. Therefore, a Fabry-Perot pressure sensor in reality can be regarded as being made up of a large number of ideal elementary Fabry-Perot pressure sensors with different cavity lengths within the optical sensing area. The final measured optical intensity distribution will be the superposition of all the intensities from all these ideal elementary Fabry-Perot pressure sensors.

Let $\hat{R}$ represent the modified reflectance of a Fabry-Perot pressure sensor with non-planar pressurized deflection, ψ' the phase advance corresponding to a double passage of light within the Fabry-Perot pressure sensor with nominal cavity length and ψ'_{ei} the phase advance error induced by non-planarity curvature of the i th elementary ideal pressure sensor, the averaged reflectance can then be calculated as:

$$\hat{R} = \frac{\sum_{i=1}^n R(\psi' + \psi'_{ei}) A_{mi}}{A_m} \quad (5.1)$$

where A_{mi} is the area of the i th infinitesimally divided mirror section and A_m is the total area of the optical sensing area.

Because of the symmetrical deflection behaviour of an edge-clamped diaphragm, the optical sensing area is divided into square strip areas as shown in Fig.5.5(a). Each square strip area is supposed to have the same deflection and can be regarded as an ideally planar mirror section. It is obvious that the finer the partition of the diaphragm is, the more accurate the simulated output optical intensity. Therefore, the least required partition number to ensure the accuracy of the simulation results needs to be determined. The averaged reflectance of planar bottom Fabry-Perot pressure sensors interconnected with a single-mode and a multimode fiber and using different numbers of partition in optical sensing area are simulated and the results are shown in Fig.5.5 (b)-(c). The optical structure as shown in Fig.5.3(b) and an external load of 20psi are used in the simulation. Planar bottom Fabry-Perot pressure sensor is chosen because the planar diaphragm displays the largest non-planarity among different types of

diaphragm when a same external load is applied. Therefore , the least required partition number which satisfies the simulation accuracy of the flat bottom pressure sensor can also guarantee the simulation accuracy of other types of pressure sensors. From Fig.5.5 (b)-(c), it can be seen that both curves are converged and therefore, for single-mode and multimode optical fibers, the optical sensing areas are divided into 20 sections and 50 sections, respectively, which is believed to be accurate enough to simulate signal averaging effect.

Fig.5.5 (a) Schematic view of the partition of the optical sensing area for nominal averaged reflectance calculation, and the simulated averaged reflectance using different numbers of partition in optical sensing area for the planar bottom pressure sensor interconnected with (b) single-mode fiber and (c) multimode fiber

5.3.3 Simulations and comparisons of signal averaging effect

As we illustrated before, two optimal Fabry-Perot optical structures as shown in Fig.5.3 will be used to thoroughly investigate the signal averaging effect of different Fabry-Perot pressure sensors interconnected with different types of optical fibers. To make discussions more general, in the following simulations, the first optimal Fabry-Perot structure operating at a wavelength of 1300nm is assumed to be interconnected with a multimode optical fiber, while the second optimal Fabry-Perot structure operating at a wavelength of 1550nm is assumed to be interconnected by a single-mode optical fiber. In order to facilitate further discussions, the optical sensing areas for different Fabry-Perot pressure sensors are first plotted as shown in Fig.5.6.

Fig.5.6 Schematic view of optical sensing areas for different Fabry-Perot pressure sensors (a) Planar bottom diaphragm, (b) Planar top diaphragm, (c) Shallowly corrugated top diaphragm, (d) Single deeply corrugated bottom diaphragm

It should be pointed out that the central flat area (i.e., the area between the pressure access holes) of the stationary diaphragm mirror should be larger than the illuminated area defined by mode-field diameter (MFD) of the optical fiber. Otherwise, part of the optical lights will directly pass through the undeflected diaphragm (stationary mirror) and the Fabry-Perot microcavities are not formed over these areas, resulting in a complex and unpredictable optical response. Therefore, optical sensing area can then be

defined by the MFD, i.e., $9.3\ \mu\text{m}$ for a single-mode optical fiber and $62.5\ \mu\text{m}$ for a multimode fiber.

a. Planar bottom diaphragm pressure sensor

The signal averaging effect of a Fabry-Perot pressure sensor using planar bottom diaphragm has been simulated and the results are shown in Fig.5.7 (a)-(b). During the simulations, the bottom diaphragm size is assumed to be $100\times 100\ \mu\text{m}^2$ and the initial stress of the diaphragm is assumed to be 70MPa. In order to thoroughly investigate the signal averaging effect, the applied pressures are assumed to be up to three times as large as the monotonic sensing pressure range and the resultant deflection becomes as large as $3/4$ of the operating wavelength. In Fig.5.7, it can be seen that the curve marked with “Ideal” is the ideal optical response of a pressure sensor having an assumed piston-like moving diaphragm and is calculated using the deflection over the whole diaphragm. On the contrary, the curve marked with “Planr bottom diaphragm” is the actual optical response which is calculated using the non-planar deflection results of a flat diaphragm with clamped boundaries as discussed in Chapter 3. Fig.5.7(a) shows that for the case of the Fabry-Perot pressure sensor using a planar bottom diaphragm interconnected with a multimode fiber, the signal averaging effect of the optical response prevails which severely influences the output optical response when the applied pressure increases. This is because, in this case, the entire optical sensing area occupies most part of the deflecting area of the edge-clamped bottom diaphragm as shown in Fig.5.6(a). On the other hand, for the same pressure sensor interconnected with single-mode fiber, much smaller signal averaging effect can be observed as shown in Fig.5.6(b). This is because the optical sensing area in this case becomes much

smaller so that the degree of curvature of the deflecting diaphragm within the optical sensing area is greatly improved.

Fig.5.7 Evaluation of signal averaging effect of the Fabry-Perot pressure sensor using a planar bottom diaphragm interconnected with (a) Multimode optical fiber (b) Single-mode optical fiber

Theoretically, since the signal averaging effect is caused by an increase of the curvature of the deflecting diaphragm within the optically sampled area, it is desirable to increase the size of the moving diaphragm, thereby reducing the effect of non-planar deflection. Unfortunately, increasing the size of the bottom diaphragm is not practical since the bottom diaphragm should be smaller than the diameter of the optical fiber cladding layer according to the structural configuration of the sensor as shown in Fig.5.6(a).

b. Planar top diaphragm pressure sensor

Since it is not practical to enlarge the deflecting bottom diaphragm size in a planar bottom diaphragm sensor, one alternative method is to use a planar top diaphragm as the deflecting diaphragm so that the optical sensing area becomes only a small portion of the entire area of the deflecting top diaphragm as shown in Fig.5.6(b). Furthermore, for a planar top diaphragm pressure sensor, since the top diaphragm size is only limited by MEMS fabrication processes, the signal averaging effect can be further reduced by enlarging the deflecting top diaphragm. The ideal and the actual optical responses of the planar top diaphragm Fabry-Perot pressure sensor using a $200 \times 200 \mu m^2$ deflecting diaphragm and a $300 \times 300 \mu m^2$ deflecting diaphragm of initial stress 70MPa have been simulated and the results are shown in Figs.5.8-5.9.

Fig.5.8 Evaluation of signal averaging effect of the Fabry-Perot pressure sensor using a planar top diaphragm ($200 \times 200 \mu\text{m}^2$) interconnected with (a) Multimode optical fiber
(b) Single-mode optical fiber

(a) Multimode ($\lambda=1300\text{nm}$) (b) Single-mode ($\lambda=1550\text{nm}$)

Fig.5.9 Evaluation of signal averaging effect of the Fabry-Perot pressure sensor using a planar top diaphragm ($300 \times 300 \mu\text{m}^2$) interconnected with (a) Multimode optical fiber
(b) Single-mode optical fiber

Fig.5.8 and Fig.5.9 show that the signal averaging effect has been evidently reduced when compared with that of a planar bottom pressure sensor. Especially, when a planar

top pressure sensor interconnected with a single mode optical fiber, signal averaging effect becomes very small and can even be neglected for the case where the deflecting diaphragm size is $300 \times 300 \mu\text{m}^2$. Comparing Fig.5.8 with Fig.5.9, it is demonstrated that the signal averaging effect can be further reduced by increasing the size of the deflecting planar top diaphragm. However, increasing the top diaphragm size will necessarily increase the undesirable micromachining process complexity and will substantially reduce the monotonic sensing range. Also, it should be pointed out that for a top deflecting diaphragm Fabry-Perot pressure sensor based on a reflectance detection scheme, the top deflecting diaphragm will bend towards the bottom diaphragm when external load is applied, as can be seen from Fig.5.1(b) and Fig.5.1(c). Since the cavity gap distance is typically about one or two times of the operating wavelengths, such bending may result in damages of the sensors due to the stick of the two diaphragms when external applied loads become large enough. These defects greatly limit the overall performance of the planar top diaphragm pressure sensor.

c. Shallowly corrugated top diaphragm pressure sensor

For a planar top diaphragm pressure sensor, the degree of curvature of the optical sensing area is still evident, especially in the case where a multimode optical fiber interconnection is assumed. Since the size of a top deflecting diaphragm cannot be increased at will due to required sensing range and micromachining process complexity, corrugations are then implemented on the planar top diaphragm to achieve further improvement in planarity of a pressurized diaphragm as shown in Fig.5.6(c). However, according to the micromachining processes, the corrugation is patterned on the air gap sacrificial layer. Therefore, the shallow corrugation depth is strictly limited to about

one half of the air gap distance, i.e., $1\text{ }\mu\text{m}$, and this evidently limits the corrugation effect on reducing the signal averaging effect. Also, it should be pointed out that the shallow corrugation needs to be placed outside of the optical sensing area to avoid the unwanted perturbations added onto the resultant optical response.

The ideal and the actual optical responses of a shallowly corrugated top diaphragm Fabry-Perot sensor are simulated and the results are shown in Fig.5.10. The diaphragm used in the simulation is a $200\times 200\text{ }\mu\text{m}^2$ diaphragm with an initial stress of 70MPa with a shallowly corrugated ring of $1\text{ }\mu\text{m}$ depth outside the optical sensing area. From Fig.5.8 and Fig.5.10, when compared a shallowly corrugated top diaphragm with a planar diaphragm of same size, one can see clearly that the improvement in the reduction of signal averaging effect does occur but is limited to a small extent. Meanwhile, the monotonic sensing range is still restricted due to the large size of the deflecting top diaphragm.

Fig.5.10 Evaluation of signal averaging effect of the Fabry-Perot pressure sensor using a shallowly corrugated top diaphragm ($200 \times 200 \mu\text{m}^2$) interconnected with (a) Multimode optical fiber (b) Single-mode optical fiber

d. SDCD bottom diaphragm pressure sensor

For the proposed novel SDCD Fabry-Perot pressure sensor, from the non-planar deflection behaviour discussed in Chapter 3, the reduction of SDCD non-planar deflection within the optical sensing area is evident when compared with those of the flat diaphragm and the shallowly corrugated diaphragm. Therefore, SDCD pressure sensors are proven to have much improved optical performance and a minimal signal averaging effect when a same size of deflecting diaphragm is considered. Meanwhile, compared with a planar top diaphragm pressure sensor and a shallowly corrugated diaphragm pressure sensor, the SDCD pressure sensor has the advantages of desirable sensing range and relatively simpler micromachining process. The ideal and the actual optical responses of Fabry-Perot sensing structures composed of a $100 \times 100 \mu\text{m}^2$ single

deeply corrugated deflecting diaphragm with corrugation depths of $40\text{ }\mu\text{m}$ and $80\text{ }\mu\text{m}$ are studied and the results are shown in Fig.5.11 and Fig.5.12.

Fig.5.11 Evaluation of signal averaging effect of the Fabry-Perot pressure sensor using a single deeply corrugated top diaphragm of size $100\times 100\text{ }\mu\text{m}^2$ and a depth $40\text{ }\mu\text{m}$ interconnected with (a) Multimode optical fiber (b) Single-mode optical fiber

Fig.5.12 Evaluation of signal averaging effect of the Fabry-Perot pressure sensor using a single deeply corrugated top diaphragm of size $100 \times 100 \mu\text{m}^2$ and a depth $80 \mu\text{m}$ interconnected with (a) Multimode optical fiber (b) Single-mode optical fiber

Similarly, from Fig.5.11 and Fig.5.12, it can be seen that with the increase of corrugation depth, signal averaging effect can be further reduced. Compared with the

optical response of a planar bottom pressure sensor with a same diaphragm size as shown in Fig.5.7, an SDCCD pressure sensor produces much improved optical response. Moreover, when compared Fig.5.11 with Fig.5.8 and Fig.5.10, the performance of an SDCCD pressure sensor with a diaphragm size of $100 \times 100 \mu\text{m}^2$ and a corrugation depth of $40 \mu\text{m}$ is even much better than the performance of a planar top diaphragm and a shallowly corrugated top diaphragm pressure sensor with a much larger diaphragm size of $200 \times 200 \mu\text{m}^2$. These results are promising since on one hand, the pressure sensor using SDCCD can provide comparable optical response as a planar top diaphragm and a shallowly corrugated top diaphragm pressure sensor. And on the other hand, the pressure sensor using SDCCD can provide much larger sensing range and reduced micromachining complexity.

e. Conclusions

In general, planar bottom diaphragm pressure sensors have obvious signal averaging effect for both single-mode and multimode fiber interconnections. For other types of pressure sensors, the signal averaging effect tends to be very small when sensors interconnected by single-mode optical fibers because of the tiny effective optical sensing area whose deflection under applied pressure is virtually piston-like.

For different pressure sensors interconnected with multimode optical fibers, the SDCCD pressure sensor displays the best overall performance, i.e., small signal averaging effect, moderate sensing range and reduced micromachining complexity. This can be well demonstrated by using the simulation results of the signal averaging effect with diaphragm deflection for different Fabry-Perot pressure sensors as shown in Fig.5.13.

Similarly, the simulation range is set to be trice the monotonic sensing range of the pressure sensor, which is 3/4 of the operating wavelength. The monotonic sensing range for each of simulated sensors, which is 1/4 of the operating wavelength, is listed in Table 5.1.

Fig.5.13 Evaluation of signal averaging effect with diaphragm deflection for different Fabry-Perot pressure sensors interconnected with multimode optical fibers

Table 5.1 Monotonic sensing range of different Fabry-Peror pressure sensors

Fabry-Perot sensor	PB	PT200	S	SDCD40
Sensing range (psi)	30	3.3	3.8	14.5

The notations used in the Fig.5.13 and Table 5.1 are indicated as below,

Ideal = Piston-like diaphragm pressure sensor with a diaphragm size of $100\times100\mu\text{m}^2$

PB = Planar bottom diaphragm pressure sensor with a diaphragm size of $100 \times 100 \mu\text{m}^2$

PT200 = Planar top diaphragm pressure sensor with a diaphragm size of $200 \times 200 \mu\text{m}^2$

S = Shallowly corrugated top diaphragm pressure sensor with a diaphragm size of $200 \times 200 \mu\text{m}^2$ and a corrugation depth of $1 \mu\text{m}$

SDCD40 = Single deeply corrugated diaphragm pressure sensor with a diaphragm size of $100 \times 100 \mu\text{m}^2$ and a corrugation depth of $40 \mu\text{m}$

5.4 Zero-pressure offset

From Fig.5.13, it should be noticed that there are differences between a shallowly corrugated diaphragm pressure sensor and an SDCD pressure sensor, when compared with an ideal Fabry-Perot pressure sensor when there are no external loads applied. This is the so-called “Zero pressure offset” which arises due to diaphragm warpage.

Following the discussions in Chapter 3, non-planar diaphragms which have internal tensile stresses usually exhibit warpages -- small initial deformations under zero externally applied pressure. Such an initial deformation changes the initial gap distance and results in an offset in the final optical output response. Since a planar diaphragm does not produce warpage under any initial tensile stress, a planar bottom pressure sensor and a planar top pressure sensor do not have zero-pressure offset, which can be clearly observed from Fig.5.13. On the other hand, for a shallowly corrugated diaphragm pressure sensor and an SDCD pressure sensor, their warpages result in

observable zero pressure offsets and contribute to the phase shifts of the original optical responses.

Although zero pressure offset has no effect on the magnitude or the linearity of the resultant optical output response, it still needs to be considered in the design and optimization of a Fabry-Perot pressure sensor when a corrugation is introduced. In our research, the warpage effect of an SDCD is already included in the optimization process for initial cavity gap distance of an SDCD pressure sensor. This is well demonstrated from Fig.5.13 in which, the non-pressurized reflectance is fitly adjusted for an SDCD pressure sensor to the required minimal value in order to enlarge the monotonic sensing range.

5.5 Thermal cross-sensitivity

Fabry-Perot microcavities are typically made of dielectric multilayered thin films. With variations of ambient temperatures, thermal expansions and thermal stresses caused by mismatches of coefficients of thermal expansion (CTEs) among materials can lead to changes in both refractive index and film layer thickness. Consequently, the quality and the optical performance of a device may become significantly deteriorated or even damaged due to phase shift or transmission degradation. Temperature dependence of center wavelength shifts of narrow bandpass filters has been reported recently in literature (A. Zoller *et al.*, 1996; S. Sakaguchi, 1999). However, for pressure sensors, such thermally induced stresses will also cause diaphragm stiffness variations and increase analysis complexity. Therefore, a more reliable analytical model needs to be provided to evaluate these thermally induced stresses and their resultant thermal cross-sensitivities based on coupled thermo-optical and stress-optical effects.

5.5.1 Thermally induced stress

For the proposed Fabry-Perot pressure sensor, the materials of the moving mirror, the stationary mirror and the substrate are shown in Fig.5.14. Because the area of each of the layers is very large as compared with its thickness, the stresses and the strains induced in each layer can generally be expressed as (H. Aben *et al.*, 1993):

$$\sigma_x = \sigma_y \neq 0, \sigma_z = 0 \quad (5.2)$$

$$\varepsilon_x = \varepsilon_y = \frac{\sigma_x - \nu(\sigma_y + \sigma_z)}{E} = \frac{(1 - \nu)\sigma_x}{E} = \frac{\sigma_x}{E^0} \quad (5.3)$$

$$\varepsilon_z = \frac{\sigma_z - \nu(\sigma_x + \sigma_y)}{E} = -\frac{2\nu\sigma_x}{E} \quad (5.4)$$

Fig.5.14 Schematic view of the materials used in the proposed Fabry-Perot pressure sensor

For the layers deposited on the substrate, if the substrate is thick and hence rigid enough to resist any bending, thermally induced strains are then given by

$$\alpha_i \Delta T + \varepsilon_{xi} = \alpha_s \Delta T - \varepsilon_{xs} \quad (5.5)$$

where subscript i represents the i th layered thin film deposited on the substrate, subscript s represents the silicon substrate and E^0 is the generalized Young's modulus which is defined as $E^0 = E/(1 - \nu)$.

Stresses generated in the mirror layers and those in the substrate should be balanced. If we denote 1 and 2 as the polysilicon and the silicon nitride layers, respectively, as shown in Fig.5.14, and σ_1 and σ_2 as the thermally induced stress in these layers, we obtain,

$$\sigma_{xs} d_s = \sum_{i=1}^5 \sigma_{xi} d_i = \sigma_1 \sum d_1 + \sigma_2 \sum d_2 \quad (5.6)$$

where $\sum d_1$ and $\sum d_2$ are the total thickness of the three polysilicon layers and the two silicon nitride layers, respectively, as shown in Fig.5.14.

Substituting Eq. (5.3) into Eq. (5.5) and combining the resultant equation with that of Eq. (5.6), we can calculate the thermally induced stresses σ_1 and σ_2 of the polysilicon layer and the silicon nitride layer, respectively, as

$$\sigma_1 = \frac{E_s^0 (\alpha_s - \alpha_1) d_s - E_2^0 (\alpha_1 - \alpha_2) \sum d_2}{\sum d_1 + \frac{E_2^0}{E_1^0} \sum d_2 + \frac{E_s^0}{E_1^0} d_s} \Delta T \quad (5.7)$$

$$\sigma_2 = \frac{E_s^0 (\alpha_s - \alpha_2) d_s + E_1^0 (\alpha_1 - \alpha_2) \sum d_1}{\frac{E_1^0}{E_2^0} \sum d_1 + \sum d_2 + \frac{E_s^0}{E_2^0} d_s} \Delta T \quad (5.8)$$

where α_s , α_1 and α_2 are the thermal expansion coefficients of the substrate (silicon), the polysilicon layer, and the silicon nitride layer, respectively.

When the substrate is much thicker than the mirror layers, which means that $d_s \gg \sum d_1$ and $d_s \gg \sum d_2$, then Eq.(5.7) and Eq.(5.8) can be simplified to become,

$$\sigma_1 = E_1^0 (\alpha_s - \alpha_1) \Delta T \quad (5.9)$$

$$\sigma_2 = E_2^0 (\alpha_s - \alpha_2) \Delta T \quad (5.10)$$

It is evident from Eqs.(5.9)-(5.10) that the thermally induced stress changes with temperature. For a multilayered diaphragm, the average thermally induced stress is given by,

$$\sigma_{eq} = \frac{\sum_i (\sigma_i d_i)}{\sum_i d_i} \quad (5.11)$$

where d_i is the thickness of i th mirror layer.

5.5.2 Modeling of thermal cross-sensitivity of Fabry-Perot sensor

For multilayered Fary-Perot cavity filters, it has been well established that the variation of optical path length in each film layer plays a dominant role in the change of transmission characteristics such as the center-wavelength shift and the degradation of the characteristic transmission curve. Similarly, for the proposed Fabry-Perot pressure sensor, when ambient temperature changes, the mechanisms leading to a temperature-dependent response mainly arise from (1) variation of the effective spring constant of the diaphragm that arises from thermal stresses and (2) variation in the mirror-layer optical path length and cavity-gap distance that is due to the coupled photoelastic and thermal-optical effects. The interactions between thermo-optical and elastic effects are shown in Fig.5.15.

Fig.5.15 Interactions among temperature, stress and optical behavior

For the thermally-induced variation of the spring constant of a diaphragm owing to its stress-stiffening effect, it can be simulated using FEA commercial software ANSYS. The simulation processes are similar to those discussed in Chapter 3. Thermal stress can be introduced in the diaphragm through the use of the “cooling temperature” method as proposed by Zhang and Wise (Y. Zhang *et al.*, 1994).

For optical length variations in Fabry-Perot microcavity, with an increase in temperature, both the refractive index, n_i and the layer thickness, d_i , of the Fabry-Perot mirror layer are changed and can be generally expressed as (S. Sakaguchi, 1999),

$$\Delta n = \left(\frac{\partial n}{\partial T} \right)_{\sigma} \Delta T + \left(\frac{\partial n}{\partial \sigma} \right)_{T} \Delta \sigma \quad (5.12)$$

$$\Delta d = \left(\frac{\partial d}{\partial T} \right)_{\sigma} \Delta T + \left(\frac{\partial d}{\partial \sigma} \right)_{T} \Delta \sigma \quad (5.13)$$

$$\Delta(nd) = d(\Delta n) + n(\Delta d) = d \left(\left(\frac{\partial n}{\partial T} \right)_{\sigma} \Delta T + \left(\frac{\partial n}{\partial \sigma} \right)_{T} \Delta \sigma \right) + \frac{n}{d} \left(\left(\frac{\partial d}{\partial T} \right)_{\sigma} \Delta T + \left(\frac{\partial d}{\partial \sigma} \right)_{T} \Delta \sigma \right) \quad (5.14)$$

where, $(\partial n / \partial T)_{\sigma}$ is the thermo-optical coefficient; $(\partial n / \partial \sigma)_{T}$ is the photoelastic (stress-optical) coefficient; $(\partial d / \partial T)_{\sigma} = \alpha d$ is the thermal expansion per degree;

$(\partial d / \partial \sigma)_{T} = \frac{2\nu}{E} d$ is the thickness variation per degree caused by thermal stress, which

can be deducted from Eq.(5.4); $\Delta \sigma$ is the thermally induced stress; and ΔT is the

temperature variation. Therefore, the overall variation of optical path length can be calculated as,

The physical properties of most of the widely used dielectric film materials, such as polysilicon and silicon nitride, have not been sufficiently clarified. According to the limited published data available, the thermo-optical coefficient, $(\partial n / \partial T)_\sigma$, and the photo-elastic coefficient, $(\partial n / \partial \sigma)_T$, of polysilicon and silicon nitride are thought to be in the order of $10^{-6} \text{ }^\circ\text{C}^{-1}$ and 10^{-12} Pa^{-1} , respectively (T. C. Chen *et al.*, 2001).

5.5.3 Thermal cross-sensitivity of Fabry-Perot sensor using flat diaphragm

Based on Eqs.(5.9)~(5.11), the change of thermally induced stress $\Delta\sigma$ for the proposed diaphragm (moving mirror) that is subjected to temperature variation can be derived to be approximately $-40\text{kPa}/^\circ\text{C}$. The range of temperatures explored in the present study is restricted to $23 \sim 150 \text{ }^\circ\text{C}$ to ensure that the mechanical properties of the materials used in our design are relatively constant over the range of temperature changes. Therefore, heating the diaphragm from a room temperature to 150°C will result in a decrease in tensile stress of 5MPa for a planar clamped diaphragm, which will significantly influence the performance of the Fabry-Perot pressure sensor. Therefore, from Eqs.(5.12) and (5.13), it can be worked out that for the said temperature range, $(\partial n / \partial T)_\sigma \Delta T$ is approximately in the order of 10^{-4} and $(\partial n / \partial \sigma)_T \Delta\sigma$ is in the order of 10^{-5} . Similarly, when we consider the thickness variation ratio, $\Delta d / d$, $(\partial d / \partial T)_\sigma \Delta T / d$ is in the order of 10^{-4} and $(\partial d / \partial \sigma)_T \Delta\sigma / d$ is in the order of 10^{-5} . Therefore, it can be seen from Eq.(5.14) that the influences of these factors on the

resultant optical response are almost equally significant and hence cannot be disregarded in the analysis of optical performance fluctuation.

Based on above discussions, the thermal cross-sensitivity of a Fabry-Perot pressure sensor is primarily dependent on two factors: the effective spring constant variation of a moving diaphragm due to stress-hardening effect and the variations in the refractive index, the mirror layer thickness and the cavity gap distance. Before the simulation of thermal cross-sensitivity, thermally induced diaphragm-stiffness variation under different ambient-temperature conditions needs to be established and this is simulated using FE software ANSYS. Using the proposed optimal optical structure shown in Fig.5.3(a), the reflectance responses of a Fabry-Perot planar bottom sensor are first simulated under different temperatures which are 25°C (room temperature), 65°C , 105°C and 145°C , respectively and the results are shown in Fig.5.16. The simulated applied pressure range is 40 psi, which corresponds to a diaphragm deflection of slightly more than $1/4$ of the optical wavelength. Fig.5.16 shows that obvious deviations of reflectance can be observed as temperature increases. Furthermore, for one monotonic sensing range within marked vertical lines as shown in the figure, an interesting phenomenon can be observed that the biggest deviation occurs in the middle of the sensing range. At both the beginning and the end of the monotonic sensing range, temperature elevation only results in very small deviations. This is because when reflectance approaches to the extremum, the reflectance fluctuations induced by the variations in cavity gap distance or mirror layer thicknesses become very small. As a result, the corresponding thermally induced optical length variation has little effect on the thermal cross-sensitivity of the sensor.

Fig.5.16 Reflectance of a planar bottom diaphragm Fabry-Perot pressure sensor under different temperatures

In order to thoroughly investigate thermal cross-sensitivity, individual contributions of diaphragm stiffness variation and optical path length variation are simulated for an external load of 15psi, and the results are shown in the Fig.5.17. Fig.5.17 demonstrates that the optical path length variation plays a more important role than diaphragm stiffness variation in resultant optical response fluctuation. Also, it can be seen that overall reflectance fluctuation can reach as high as 8% when an ambient temperature elevated of 150 °C is assumed, and this significantly deteriorates the overall performance of the pressure sensor. Therefore, it is necessary to introduce SDCD to the Fabry-Perot pressure sensor in order to reduce the temperature effect.

Fig.5.17 Thermal cross-sensitivity of a planar bottom diaphragm Fabry-Perot pressure sensor under an external load of 15 psi ($\lambda = 1300\text{nm}$)

5.5.4 Reduction of thermal stress in the proposed SDCD

Fig.5.18 shows a schematic view of the proposed SDCD, which consists of a flat bottom region that is suspended on all sides by 4 free sidewalls. As we illustrated before, the flat bottom-region that serves as the moving mirror of the Fabry-Perot microcavity is thought to behave as a normal flat diaphragm. The flexible suspended sidewalls are expected to have buffering and stress-releasing effect for the moving mirror, thereby enhancing the degree of flatness of the moving mirror under pressurized deflection and reducing the temperature dependence of the sensor performance.

Fig.5.18 Definition of geometry and boundary conditions of the proposed SDCD

A unique feature that marks this design is that it employs a single deep corrugation that enhances the flatness of the pressurized deflection of the moving mirror while releasing the thermally induced stress at the same time. Therefore, both an improved signal-averaging effect and a reduced temperature cross-sensitivity can be expected in optical performance of an SDCD Fabry-Perot pressure sensor. The improved signal-averaging effect has been fully demonstrated in our previous discussions. Here we will concentrate on the study of thermal cross-sensitivity of an SDCD Fabry-Perot sensor.

For the SDCD, the suspended sidewalls act as decoupling zone and transform loads at the outer edges into local deformations with very small effect on the deformations of the central area of the zone, i.e., the sensing area. Therefore, the thermally induced stress of SDCD bottom region can be greatly reduced when compared with that of a flat diaphragm. In the analytical model the SDCD is divided into three parts, (1) the bottom region (sensing area); (2) the decoupling zone; (3) the outer region, as shown in Fig.5.18.

According to the SDCD structure, the suspended sidewalls form a conical shell that can be defined by its thickness d ; its inner half side length a ; its depth H_d ; its angle α and

its out half side length b , where $b = a + H_d / \tan \alpha$. The solutions for the radial displacement $u(r)$, the rotations $\psi(r)$, the radial component of the internal force per unit length $F(r)$, and the bending moment per unit length $M(r)$ as a function of the radial coordinate r ($a \leq r \leq b$) then become (F. I. Niordson, 1985),

$$u(r) = \frac{D\tau \cos \alpha}{Ed} [A_1 (2\nu \text{bei}_2 x - x \text{bei}_2' x) - A_2 (2\nu \text{ber}_2 x - x \text{ber}_2' x) + B_1 (2\nu \text{kei}_2 x - x \text{kei}_2' x) - B_2 (2\nu \text{ker}_2 x - x \text{ker}_2' x)] \quad (5.15)$$

$$F(r) = \frac{-16D\tau}{x^2 \cos \alpha} (A_1 \text{bei}_2 x - A_2 \text{ber}_2 x + B_1 \text{kei}_2 x - B_2 \text{ker}_2 x) \quad (5.16)$$

$$\psi(r) = A_1 \text{ber}_2 x + A_2 \text{bei}_2 x + B_1 \text{ker}_2 x + B_2 \text{kei}_2 x \quad (5.17)$$

$$M(r) = \frac{4D\tau \tan \alpha}{x^2} [A_1 (2\nu \text{ber}_2 x + x \text{ber}_2' x) + A_2 (2\nu \text{bei}_2 x + x \text{bei}_2' x) + B_1 (2\nu \text{ker}_2 x + x \text{ker}_2' x) + B_2 (2\nu \text{kei}_2 x + x \text{kei}_2' x)] \quad (5.18)$$

$$\tau = \frac{\sqrt{3(1-\nu^2)}}{d}, \quad x = \sqrt{\frac{8\tau r \tan \alpha}{\cos \alpha}}, \quad D = \frac{Ed^3}{12(1-\nu^2)} \quad (5.19)$$

where $a = r_{in} < r < r_{out} = b$. From Eq.(5.19) it can be seen that x is determined by r .

Defining x_{in} and x_{out} as the value of x when $r = r_{in}$ and $r = r_{out}$, respectively, it can be calculated that $x > x_{in} = 33$ for the proposed SDCD with half side length a of $50 \mu m$ and diaphragm thickness d of $1.5 \mu m$, while x_{out} depends on the corrugation depth H_d . The functions $\text{ber}_2, \text{bei}_2, \text{ker}_2$ and kei_2 are Kelvin functions of the second order that can be expressed in terms of Kelvin functions of the zeroth order and their

derivatives (M. Abramowitz, 1970). According to Niordson, when x is a large value ($x > 25$), the second-order Kelvin functions can be approximated by (F. I. Niordson, 1985),

$$\text{ber}_2 x \approx -\text{ber} x \approx -\sqrt{\frac{1}{2\pi x}} \cos\left(\frac{x}{\sqrt{2}} - \frac{\pi}{8}\right) \exp\left(\frac{x}{\sqrt{2}}\right) \quad (5.20)$$

$$\text{bei}_2 x \approx -\text{bei} x \approx -\sqrt{\frac{1}{2\pi x}} \sin\left(\frac{x}{\sqrt{2}} - \frac{\pi}{8}\right) \exp\left(\frac{x}{\sqrt{2}}\right) \quad (5.21)$$

$$\text{ker}_2 x \approx -\text{ker} x \approx -\sqrt{\frac{\pi}{2x}} \cos\left(\frac{x}{\sqrt{2}} + \frac{\pi}{8}\right) \exp\left(-\frac{x}{\sqrt{2}}\right) \quad (5.22)$$

$$\text{kei}_2 x \approx -\text{kei} x \approx \sqrt{\frac{\pi}{2x}} \sin\left(\frac{x}{\sqrt{2}} + \frac{\pi}{8}\right) \exp\left(-\frac{x}{\sqrt{2}}\right) \quad (5.23)$$

The constants A_1, A_2, B_1, B_2 can be determined from the boundary conditions. Based on Eqs.(5.16) and (5.20)~(5.23) and by ignoring the infinitesimal parts $\text{ker}_2 x$ and $\text{kei}_2 x$ in the corresponding expression, the reduction of force and moment in the inner part (bottom region) can be approximated as,

$$\frac{F_{out}}{F_{in}} \approx \frac{M_{out}}{M_{in}} \approx C\left(\frac{a}{b}\right)^{5/2} \exp[K(H_d)] = C\left(\frac{1}{1 + H_d/(a \cdot \tan \alpha)}\right)^{5/2} \exp[K(H_d)] \quad (5.24)$$

$$K(H_d) = \frac{x_{out} - x_{in}}{\sqrt{2}} = \frac{2\sqrt{a \cdot \sin \alpha} ((3(1 - \nu^2))^{1/4} (\sqrt{1 + H_d/(a \cdot \tan \alpha)} - 1))}{\sqrt{d} \cos \alpha} \quad (5.25)$$

For Eq.(5.24), C is assumed to be 1 to maintain the continuity that the equation is also valid when the corrugation depth H_d is zero. If only the temperature variation is considered and no external loads are applied, the generated forces and moments in the SDCD bottom region and the outer diaphragm are all directly related to the thermally induced stress. Therefore, the thermal stress reduction can be defined as,

$$\frac{\sigma_{out}}{\sigma_{in}} \approx \left(\frac{1}{1 + H_d / (a \cdot \tan \alpha)} \right)^{5/2} \exp[K(H_d)] \quad (5.26)$$

where σ_{out} is the thermally induced stress in the outer region of an SDCD, σ_{in} is the thermally induced stress in bottom region or sensing area of an SDCD and $K(H_d)$ is computed based on Eq. (5.25).

The curve of stress reduction factor shown in Fig.5.19 is plotted for SDCD structures with a fixed thickness of $d = 1.5\mu m$, half width $a = 50\mu m$, $\alpha = 54.74^\circ$ over different corrugation depths H_d . From Fig.5.19, we can see that with the increase of corrugation depth, the thermally induced stress in SDCD bottom region can be further reduced. Since the corrugation depth is chosen as $40\mu m$ in the present study, the reduction factor can in this case reach as high as 200, thereby sufficiently significantly releasing the thermally induced stress.

Fig.5.19 Thermal stress reduction factor of the proposed SDCD as a function of corrugation depth

5.5.5 Thermal cross-sensitivity of Fabry-Perot sensor using SDCD

As we proposed earlier in the Section 5.5.2, the thermal cross-sensitivity of a Fabry-Perot pressure sensor is determined primarily by the variations in diaphragm stiffness and in optical path length. Because an SDCD successfully release thermally induced stress over the sensing area as much as 200 times, both diaphragm stiffness and optical path length variations can hence be greatly reduced as compared with those of a flat diaphragm. If a temperature increase of 100°C is considered, based on Eqs.(5.12)-(5.13), $(\partial n / \partial \sigma)_T \Delta \sigma$ and $(\partial d / \partial \sigma)_T \Delta \sigma / d$ will then be in the order of 10^{-7} , whereas $(\partial n / \partial T)_\sigma \Delta T$ and $(\partial d / \partial T)_\sigma \Delta T / d$ will still be in the order of 10^{-4} . Therefore, based on Eq.(5.14) it can be seen that for an SDCD structure, the $(\partial n / \partial \sigma)_T \Delta \sigma$ part in Δn and the $(\partial d / \partial \sigma)_T \Delta \sigma / d$ part in $\Delta d / d$, which are induced by thermal stress, will

become sufficiently small in this case and hence can be omitted. This can result in a further considerable reduction of the resultant optical response fluctuation.

Similarly, using the optimal optical structure shown in Fig.5.3(a), the reflectance responses of a Fabry-Perot pressure sensor using SDCD are first simulated under different temperatures of 25°C , 65°C , 105°C and 145°C , and the results are shown in Fig.5.20. The simulated pressure range is 15 psi, which corresponds to a diaphragm deflection of slightly more than $1/4$ of the optical wavelength. Compared with Fig.5.16, no obvious reflectance deviations can be observed when temperature varies from 25°C to 145°C as shown in Fig.5.20. Therefore thermally induced perturbations have been successfully reduced, especially for those measurements taken in the middle of the monotonic sensing range.

Fig.5.20 Reflectance of an SDCD Fabry-Perot pressure sensor under different temperatures

Also, thermal cross-sensitivity of a Fabry-Perot pressure sensor with an SDCD as well as the individual contributions of diaphragm stiffness variation and optical path length variation is simulated for an external load in the middle of the monotonic range such as 8 psi and the results are shown in the Fig. 5.21. From Fig.5.21, we can see that, as compared with a planar diaphragm pressure sensor whose cross-sensitivity is shown in Fig.5.17, the influence of diaphragm-stiffness variation is reduced to almost zero in this case and that the influence of optical path variation on optical response fluctuation is also reduced to a much lower level. By replacing a conventional planar diaphragm with an SDCD, thermal cross-sensitivity of a Fabry-Perot pressure sensor has been successfully reduced from 330 ppm/°C to 30 ppm/°C, thereby significantly improving the performance of the sensor (D. G. Guo *et al.*, 2005).

Fig.5.21 Thermal cross-sensitivity of an SDCD Fabry-Perot pressure sensor under an external load of 8psi ($\lambda = 1300\text{nm}$)

5.6 Summary

In this chapter, two critical stumbling blocks facing the successful realization of membrane-type Fabry-Perot pressure sensor, i.e., the signal averaging effect and the cross-sensitivity to temperature, have been thoroughly modeled and investigated.

Based on the location and the shape of a deflecting diaphragm, Fabry-Perot pressure sensors are first classified into four types, i.e, the planar bottom diaphragm pressure sensor, the planar top diaphragm pressure sensor, the shallowly corrugated top diaphragm pressure sensor and the SDCD bottom diaphragm pressure sensor. Then, the optical fiber, the optical source and the operating wavelength used for Fabry-Perot pressure sensors are discussed in detail. The signal averaging effect is elaborately modeled and simulated for different types of Fabry-Perot pressure sensor. Results show that a pressure sensor using an SDCD can significantly reduce signal averaging effect while providing preferable sensing range and micromachining capability. However, Fabry-Perot pressure sensor using a corrugated diaphragm has zero pressure offset which is induced by initial warpage. Zero pressure offset results in phase shifts of the original optical responses and needs to be considered in the design of a Fabry-Perot pressure sensor when corrugated diaphragms are to be used.

Based on coupled thermo-optical and stress-optical effects, thermally induced stress and its resultant thermal cross-sensitivity of Fabry-Perot pressure sensors are modeled and evaluated. Various factors, including the variation in effective diaphragm stiffness owing to stress-stiffening effect and the variation in the optical path length are successfully incorporated in the modeling simulation. Analytical expressions are presented to analyze stress reduction of SDCD decoupling zone. Significant stress

release effect of an SDCD and its resultant low thermal cross-sensitivity of an SDCD pressure sensor are demonstrated. It can be observed that by replacing a conventional flat diaphragm with an SDCD in a Fabry-Perot pressure sensor, thermal cross-sensitivity can be reduced by as much as 10 times.

Chapter 6

Fabrication and Measurement

6.1 Introduction

Silicon processing technology has evolved into a mature industry over the last 40 years. In the last 15 years, processing techniques developed and refined to enable large-scale integrated circuit fabrication have been modified to produce microelectromechanical systems. MEMS devices have been incorporated into many products including pressure sensors, accelerometers, and ink jet printer heads. Fabry-Perot based devices such as pressure sensors and tunable filters have been built conventionally by using a hybrid two-wafer assembly technique that requires a fusion bonding process (B. Halg, 1992; J. H. Jerman *et al.*, 1990; M. A. Chan *et al.*, 1994). The fusion bonding process limits the flexibility and the accuracy of device structural dimensions, restricts the compatibility with conventional very large scale integration (VLSI) fabrication technology and leads to low manufacturing yield. The surface micromachining method has recently emerged as a way to overcome these drawbacks (P. M. Sarro *et al.*, 1994; K. Aratani *et al.*, 1994; Y. M. Kim *et al.*, 1995; J. Han *et al.*, 2000). Surface micromachining techniques eliminate the requirement of fusion bonding and instead, they etch sacrificial layers to form high quality cavities with air gaps, achieving higher yield and better performance through precisely controlled thickness of the cavity gap.

6.2 Fabrication

6.2.1 Process flow

A sample of the proposed Fabry-Perot pressure sensor using SDCCD, as shown in Fig.6.1, has been fabricated on a single chip using both surface and bulk micromachining techniques. The sensor consists of a moving mirror formed by two polysilicon layers sandwiched with a silicon nitride layer with a thickness of 500nm/500nm/500nm ($polySi/Si_xN_y/polySi$), a stationary mirror comprises of a polysilicon layer and a silicon nitride layer with a thickness of 350nm/200nm ($polySi/Si_3N_4$), and an air gap whose thickness is $1.6\ \mu m$.

Fig.6.1 Schematic view of the proposed Fabry-Perot pressure sensor using SDCCD

The fabrication process flow is listed in the table 6.1 and the cross-sectional diagrams of different stages of the fabrication are shown in Fig.6.2. All of the fabrication steps are performed in the Micro Machines Centre of the School of Mechanical and Production Engineering at Nanyang Technological University and some fabrication details will be elaborately introduced in the next section.

The fabrication process steps are shown in Fig.6.2. The process starts with a double-side polished n-type silicon (100) wafer. Then silicon nitride layers of $0.25\ \mu\text{m}$ are formed by low-pressure chemical vapour deposition (LPCVD) on both sides of the silicon wafer and these layers are used as the mask for subsequent KOH etching, as shown in Fig.6.2(a). Fig.6.2(b) shows the patterned silicon nitride mask using lithography. Reactive-ion etching (RIE) is then performed to etch silicon nitride mask and to form the pattern required for silicon etching, as shown in Fig.6.2(c). This is followed by a KOH etching and as a result, a single deep cavity is formed as shown in Fig.6.2(d). Wet etching using hot phosphoric acid is then used to remove the silicon nitride on both sides of the silicon wafer as shown in Fig.6.2(e). After this, wafer cleaning is performed to remove any contamination generated during previous steps.

Afterwards, a layer of low-stress silicon nitride is formed by LPCVD on both sides. The LPCVD silicon nitride layer, that is deposited on the front-side of silicon wafer, is then removed by CF_4 dry etching, as shown in Fig.6.2(f). Following this, a low-stress silicon nitride layer and an undoped polysilicon layer are deposited by LPCVD to form the fixed mirror required, as shown in Fig.6.2(g). Deposition of a layer of phosphosilicate glass (PSG), which forms the sacrificial layer, is then carried out as

shown in Fig.6.2(h). A reflow of the PSG is performed at a temperature of 850 °C for 1 hour to eliminate the effect of stress concentration around sharp corners.

Table 6.1 Main process flow in device fabrication

Sequence	Process	Objective
1	LPCVD Silicon nitride	Mask for KOH etching
2	Lithography	Pattern the silicon nitride mask for KOH etching
3	RIE	To etch silicon nitride mask to form pattern for silicon etching
4	KOH etching	Formation of a single deep cavity for the growth of diaphragm
5	Wet etching using Hot Phosphoric Acid	Removal of silicon nitride on both sides of the silicon wafer
6	LPCVD Silicon nitride	Pattern the silicon nitride mask for subsequent removal of sacrificial layer
7	LPCVD Silicon nitride and Polysilicon	Formations of silicon nitride layer and polysilicon layer for the fixed mirror
8	LPCVD PSG	Deposition of sacrificial layer for its subsequent release to form the air gap
9	LPCVD Silicon nitrides and Polysilicon	Formations of silicon nitride layer, polysilicon layer and second silicon nitride layer for the moving mirror
10	RIE and buffered HF solution etching	Removal of the unwanted structures on the backside of the silicon wafer
11	RIE	Creation of etching windows for the release of sacrificial layer
12	40% HF solution etching	Removal of sacrificial layer and release of diaphragm/moving mirror

Fig.6.2 Schematic view of the fabrication processes for the proposed Fabry-Perot pressure sensor using SDCD

The three layers of the deflecting diaphragm or the moving mirror, i.e., one low-stress LPCVD silicon nitride layer sandwiched by two polysilicon layers, are deposited successively as shown in Fig.6.2(i). The composite triple structural layers and the PSG layer at the backside of the wafer are then removed by reactive ion etching (RIE) and buffered HF solution, respectively, while the front side is protected with photoresist coating as shown in Fig.6.2(j).

Etching windows are then opened by RIE as shown in Fig.6.2(k). The diaphragm is finally released in 40% HF solution as shown in Fig.6.2(l). Sequential replacement of acetone and methanol is carefully processed to protect the diaphragm from sticking.

6.2.2 Fabrication details

Mask design

The first step in MEMS device fabrication is to design masks according to the structure of the microsensor which in this case is the Fabry-Perot microsensor. Many types of software can be used to design masks, such as AutoCAD and L-Edit. In our fabrication, L-Edit software is selected. The design drawing is shown in Fig.6.3. There are two masks used in our fabrication: Mask #1, shown in red color, is used for patterning silicon nitride mask for subsequent silicon KOH etching from the front side while Mask #2, shown in green color, is used for patterning the backplate holes through which the sacrificial layer can be removed. These two masks are printed on quartz and are used during photolithography.

Fig.6.3. Mask designs for the proposed Fabry-Perot pressure sensor

Photolithography and pattern transfer

Lithography in MEMS context is typically the transfer of a pattern onto a photosensitive material by selective exposure to radiation sources such as UV (ultraviolet) light. A photosensitive material is a material that experiences a change in its physical properties when exposed to a radiation source. In lithography, the photosensitive material used is typically a photoresist.

When the photoresist is exposed to a radiation source of a specific wavelength, the chemical resistance of the resist to the developer solution has been changed. If the resist is placed in a developer solution after selective exposure to a light source, it will etch away one of the two regions (exposed or unexposed). As shown in Fig.6.4, if the exposed material is etched away by the developer and the unexposed region is resilient,

the material is considered to be a positive resist. If the exposed material is resilient to the developer and the unexposed region is etched away, it is considered to be a negative resist.

Fig.6.4 Schematic view of exposure and development of positive and negative photoresist

In our fabrication, AZ9260 is chosen as the photosensitive material due to its sufficient exposure time so that the post baking can be omitted. AZ9260 is a thick positive photoresist and the process steps of photolithography are described in details as follows. Cleaning of silicon wafer and dehydration bake (on a hotplate at 110°C for 10 minutes) are first carried out to remove any dirt and moisture adsorbed by the wafer. This is followed by the coating of HMDS, which is used to promote adhesion.

Subsequently, the wafers are spin coated with AZ9260 photoresist at 1500rpm, for 30 seconds. After the wafers are soft baked at 110°C for 4 minutes, they are left aside for a minimum of 5 minutes to let the photoresist to cool down. This is further followed by the UV exposure of 30 seconds. Subsequently, the wafers are developed in the developer solution of AZ421K for approximately 2 minutes and are rinsed in the de-ionized water. The patterned wafers are then dried and hard baked for about 15 minutes on a hotplate at a temperature of 120°C.

RIE

During the fabrication, RIE is used to etch the silicon nitride mask to form the pattern for silicon wet etching (Fig.6.2(c)) and to open etching windows for the removal of the sacrificial layer (Fig.6.2(k)). Mixed gases of CF_4 and O_2 with a ratio of 9:1 are used in the etching. The operating power is set to 350 Watts which controls the density of the ions and their energies as they impinge on the wafer. The pressure is set during the etching as 200 mtorr and the etching time is about 8 minutes.

LPCVD

LPCVD of different materials are performed in the fabrication, as summarized in the following.

a. Silicon nitride

Chemicals of SiH_4 and NH_3 are used in the deposition of silicon nitride and the operating temperature is set to be about 820°C.

b. Polysilicon

The deposition rate for the polysilicon is about 10nm per minute. The operating temperature and pressure are 605 °C and 550 mtorr, respectively.

c. PSG

PSG deposited using LPCVD stands out as one of the best materials suitable for sacrificial layers because it can be etched away even more rapidly in HF than silicon oxide. However, in order to obtain a uniform etch rate, PSG film layers must firstly be densified by heating the wafer to 950-1100 °C in a furnace for 30 minutes to 1 hour in a wet oxygen ambient or a rapid thermal annealer (RTA).

Furthermore, since the edges of the etched windows need to be tapered to minimize coverage problems with subsequent structural layers, reflow is required after patterning in a dry etch. During the reflow, the viscous flow at high temperature can smooth the edge tape. The higher the phosphorous concentration in the viscous flow is, the smoother the profiles will become after the reflow.

6.2.3 Fabrication results

Based on the process flow illustrated in Section 6.2, the proposed Fabry-Perot sensor using SDCCD is fabricated. The SEM photos of the bottom view of a single Fabry-Perot sensor and a top view of 5×5 Fabry-Perot sensor array are shown in Fig.6.5 (a)-(b).

(a)

(b)

Fig.6.5 SEM photos of (a) bottom view of a single Fabry-Perot sensor using SDCD and
(b) top view of a 5×5 Fabry-Perot sensor array using SDCD

6.3 Measurement

6.3.1 Proposed optical measurement setup

The schematic view of the thermally incorporated optical measurement setup for the proposed Fabry-Perot pressure sensor is sketched in Fig.6.6. It consists of a laser diode that illuminates the proposed and fabricated Fabry-Perot pressure sensor through a 2×2 coupler. One of the output ports is connected to the Fabry-Perot pressure sensor that is placed in a thermally-incorporated pressure cell. The other output port is dumped into the index matching liquid to avoid any reflected light from this unused port of the directional coupler. The details of the pressure cell are photographed as shown in Fig.6.7. It is equipped with an anti-reflection coated quartz window and is capable of undertaking pressure from 0 to 100 psi and temperature from 0 to 500°C. The applied pressure is provided by a dry air supply and the pressure level can be accurately monitored by an MKS pressure transducer and regulated by an air pressure regulator. Cartridge heaters are installed in the cell interlayer to regulate the temperature and a thermocouple sensor is used to monitor the cavity temperature. A diced wafer of the Fabry-Perot pressure sensor can be assembled onto the pressure cell via a retaining washer and a die holder. Three tighten screws are used to secure the sample and to seal the pressure cell. The entire pressure cell is mounted on a Standa motorized translation xyz microstage to permit the adjustment of the cell's position with respect to laser source for measurement purpose (D.G.Guo *et al.*, 2005).

Fig.6.6 Schematic view of the proposed optical measurement setup

Fig.6.7 The photograph of the thermally incorporated pressure cell (a) front view (b) back view

The entire measurement setup as well as the left view and the right view of the setup used during measurement are photographed as shown in Fig.6.8.

Fig.6.8 Photographs of (a) the whole experiment setup (b) the left view (c) the right view

It can be seen from Fig.6.6 that the transmitted light intensity can be measured by placing the photodetector right after the pressure cell and the reflected light intensity can be measured by another photodetector from one end of input ports of the 2×2 coupler. The measured reflectance can then be calculated as the ratio between the measured reflected intensity and the incident light intensity from the laser source. On the other hand, since the transmitted light intensity will pass through the anti-reflection coated quartz window which is placed on the backside of the cell, the measured transmittance is calculated as the ratio between the transmitted light intensity of a Fabry-Perot microcavity and that of a pinhole structure (no Fabry-Perot microcavity) which has the same size of the optical beam transmission area. This is reasonable because the measured transmitted light intensity of the pinhole corresponds to the transmittance value of unity.

6.3.2 Optical measurement

The wavelength-dependent transmittance of the Fabry-Perot pressure sensor was first measured without any external pressure load. The measurements and the corresponding simulated results are shown in Fig.6.9. Included in the simulation model is the variation of the refractive index of each film as a function of wavelength, using the data from (E. D. Palik, 1985). It is noticed from Fig.6.9 that excellent agreement is obtained between the measured and simulated transmittances. This not only validates the novel Gaussian optics based model for Fabry-Perot microcavity but also indicates that the optical properties of the Fabry-Perot composite mirror layers, such as refractive indices, used in previous designs are reliable and correct.

Fig.6.9 Simulated and measured transmittance as a function of the operating wavelength

6.3.3 Characterization of the fabricated Fabry-Perot sensor

The initial stress of the composite sensing diaphragm of the Fabry-Perot pressure sensor is first measured by a Tencor FLX-2908 laser system using the wafer-curvature method. However, since the multilayered sensing diaphragm is deposited on top of the fixed composite diaphragm, as well as the sacrificial layer during fabrication processes, the overall initial stress of the entire film layer assembly will be obtained during such stress measurement, instead of the stress of the sensing diaphragm as required. Therefore, during the micromachining process as shown in Fig.6.2(i), a new standard raw wafer is used together with the half processed wafer of Fabry-Perot sensors during the LPCVD process for the deposition of the multilayered sensing diaphragm. Then the actual initial stress of the sensing diaphragm of the proposed Fabry-Perot sensor can be obtained through the indirect measurement of the film stress of new raw wafer used on

which the same diaphragm layers are deposited. The resultant measured initial stress of the multilayered sensing diaphragm is about 90MPa, which is quite close to the assumed value used in the designs and simulations.

The transmittance and reflectance of the fabricated Fabry-Perot sensor are then measured under different applied pressures and under room temperature. The measurements and their corresponding simulated results are shown in Fig.6.10. From Fig.6.10, one can see that reasonable agreements are obtained between the measured and simulated transmittances as well as the reflectances, indicating that the mechanical properties of the Fabry-Perot sensor used in the simulations are correct, and the structure proposed in this study is feasible to be realized as a pressure sensor. The deviation from theoretical analysis is mainly caused by the deviation of multilayered mirror thicknesses from designed values which are induced by micromachining process. Meanwhile, the limitation of the accuracy of MKS pressure transducer and photodetector might also cause the deviation.

Fig.6.10 Simulated and measured (a) transmittance; and (b) reflectance as a function of applied pressure under room temperature of 25°C ($\lambda = 1300\text{nm}$)

6.3.4 Characterization of thermal cross-sensitivity

The temperature dependent response of the Fabry-Perot sensor was measured. Currently, the temperature increase is restricted to 125°C to avoid the melt of the anti-reflection layer on quartz window and to ensure that the mechanical properties of materials over the temperature range is virtually constant. The measured responses as a function of the differential pressure at different ambient temperatures for the proposed FP microcavity pressure sensor are shown in Fig.6.11 for the case of $\lambda = 1300\text{nm}$. As expected, the thermal cross-sensitivity measured experimentally is about $100\text{ ppm}/^{\circ}\text{C}$. Compared with the simulated thermal cross sensitivity, a larger value of the measured thermal cross-sensitivity is believed to be attributed to the slightly different multilayer thin film physical properties resulting from the temperature elevation. The stress

release effect of the corrugated diaphragm is demonstrated through the resultant low temperature dependent response. Another interesting phenomenon observed from Fig.6.11 is that for SDCD, except for the region of low pressures, both the simulated and the measured responses in the region of higher pressures show less dependence on temperature variations. The simulated thermal cross-sensitivity is reduced from 60 ppm/°C to 40 ppm/°C when external loads are increased from 10 psi to 30 psi.

Fig.6.11 Simulated and measured transmittances as a function of ambient temperatures at different pressures (10psi, 20psi, 30psi) with $\lambda = 1300 \text{ nm}$

6.4 Summary

In this chapter, fabrication of the proposed Fabry-Perot pressure sensor using SDCD is elaborately illustrated. The designed process flow and some key fabrication details are introduced. The SEM photos of the fabricated sensors are also presented.

An optical measurement setup, which includes a thermally incorporated pressure cell, is designed to characterize the fabricated Fabry-Perot sensors. The measured wavelength-dependent transmittances have been used to validate the novel Gaussian optics based model for Fabry-Perot microcavity, as well as the optical properties of the composite mirror layers used in the designs. Carefully measured pressure-dependent transmittances under room temperature confirms that the mechanical properties of the Fabry-Perot sensor used in the simulation model are reliable and correct, and the structure proposed in this study is feasible to be realized as a pressure sensor. Finally, the measurement of the temperature dependent response clearly and successfully demonstrates the stress release effect of the SDCD, as well as the corresponding low thermal cross-sensitivity of the proposed Fabry-Perot pressure sensor using SDCD.

Chapter 7

Conclusions and Recommendations

7.1 Conclusions

Optical fiber sensors have been demonstrated to be attractive for the measurement of a wide variety of physical, chemical, and biomedical parameters because of a number of inherent advantages. Some of the major advantages over conventional mechanical, electrical and electronic sensors include immunity to electromagnetic interferences, small size and light weight, avoidance of ground loops and electric sparks, resistance to harsh environments, remote operations and capability of multiplexing. Among different types of optical fiber sensors developed to date, the intensity-modulated extrinsic Fabry-Perot pressure sensor was singled out for our study because of its high spatial resolution, simple signal demodulation, absolute measurement and single-chip fabrication. However, some common issues, such as signal-averaging effect induced by the non-planar pressurized deflection, thermal cross-sensitivity, and measurement errors arising from process-induced variations in mirror layer thickness, are major stumbling blocks facing the successful realization of this type of pressure sensors.

This dissertation proposed a high-performance single deeply corrugated diaphragm (SDCD) to enable optical sensing technology to be better realized with MEMS microfabrication technology. The overall optical characteristic model of a Fabry-Perot microcavity with multilayered mirrors illuminated by laser beam was established, for the first time, using Gaussian beam optics in combination with Smith's method. The

measurement errors arising from process-induced variations in mirror layer thickness was investigated and a comprehensive optimization model was established to achieve both the preferable sensing performance and the least measurement tolerance for high yield of sensors during batch fabrication. A mathematical analysis was developed to describe the stress reduction of SDCD decoupling zone. The signal averaging effects and the thermal cross-sensitivities were thoroughly modeled with extensive numerical simulations for extrinsic Fabry-Perot pressure sensors with different types of deflecting diaphragm. The proposed Fabry-Perot pressure sensor using SDCD has been successfully fabricated and characterized by a novel thermally incorporated optical measurement setup. Conclusions and contributions of the research work undertaken are summarised as follows, and a list of publications based on the work presented in this dissertation is included in Author's Publication.

I. Development of Fabry-Perot pressure sensor with the proposed SDCD

Theoretical analyses were performed on the load-deflection behavior of four different sensing diaphragm designs, namely, the flat diaphragm, the shallowly corrugated diaphragm, the flat diaphragm with rigid center and the single deeply corrugated diaphragm. It has been observed that the problem of high initial stress in diaphragm can be successfully addressed by the introduction of a single deep corrugation. Unlike the shallow corrugation technique, the proposed single deep corrugation technique has the advantage of significantly reducing its initial stress without any increase in its bending stiffness of the diaphragm. In this way, the highest sensitivity can be achieved when a single deeply corrugate diaphragm is used as sensing diaphragm.

Also, the non-planar behavior under pressurized deflection for different diaphragm designs was studied using FEM commercial software ANSYS. From the FEA results, it has been demonstrated that, compared with flat diaphragm and shallowly corrugated diaphragm, the reduction of non-planar deflection of SDCD can reach as high as 10 times and 7 times in the case where a practically realistic corrugation depth of $100\text{ }\mu\text{m}$ is assumed. This almost piston-like travel property is very attractive since the improvement in the pressurized diaphragm flatness will reduce the signal averaging effect of the output optical response, leading to better performance of the optical sensing structure.

A pressure sensor using SDCD for applications of large measurement range was developed. Each part of the sensor, such as the sensing diaphragm, the backplate and the air gap thickness, was elaborately designed in detail. The calculated overall sensitivity of the whole pressure sensor is $10.1\text{ }\mu\text{W} / \text{kPa}$ and the sensing range of differential pressure is about 1atm. For the proposed pressure sensor, it can be applied to measure dynamic pressure signals with a highest frequency component of 200kHz, which is sufficiently high for any practical applications.

II. Optical modeling and optimization of Fabry-Perot microcavity

The optical characteristics of a Fabry-Perot microcavity with multilayered mirrors illuminated by laser beam have been successfully modeled using Gaussian beam optics in combination with Smith's method. The novel analytical model was validated through comparison with models established using different plane wave based techniques, such as Macleod characteristic matrix technique, the transfer matrix technique and Smith's technique. The influence of laser beam waist on microcavity optical response has also

been investigated and it has been established that laser beam waist size over 10 times more than the cavity length should be used so that the optical performance of Fabry-Perot microcavity illuminated by laser beam can be accurately predicted using plane wave based theory.

For a Fabry-Perot microcavity used for sensing applications, the measurement error arising from process-induced variations in mirror layer thickness was studied. A novel optimization model was then established to achieve both a preferable sensing performance and the least measurement error for required high yield of sensors during batch fabrication. The optimal Fabry-Perot microcavities were obtained for both transmittance and reflection detection schemes and under different operating wavelengths of 1300nm and 1550nm. For the proposed optimal Fabry-Perot microcavities, they can constrain the percentage nonlinearity error within 6% and the worst-case measurement error to be within 10%, while at least 94% yield of sensors during batch fabrication can be ensured.

The optical response perturbations induced by non-normal incidence as well as different types of microcavity mirror imperfections such as non-parallelism, initial residual spherical curvature and surface roughness, have been modeled. Simulation results have shown that for an optimal Fabry-Perot microcavity using transmittance detection scheme and under wavelength of 1550nm, when the non-normal incidence angle, the initial spherical curvature and the RMS surface roughness are controlled to be within 5° , 10nm and 8nm respectively, the induced perturbations in the overall optical responses can be brought to be within 1%.

III. Characterization of signal averaging effect and thermal cross-sensitivity

The signal averaging effect was elaborately modeled and simulated for extrinsic Fabry-Perot pressure sensors with different types of deflecting diaphragm. Results have shown that the Fabry-Perot pressure sensor using a SDCD can significantly reduce signal averaging effect, owing to the planarity improvement of deflecting diaphragm within optical sensing area. However, the Fabry-Perot pressure sensor using a corrugated diaphragm does produce zero pressure offset which is induced by initial warpage. Zero pressure offset results in phase shift of the original optical response and needs to be included in the design of Fabry-Perot microcavity.

Based on the coupled thermo-optical and stress-optical effects, the thermally induced stress and the resultant thermal cross-sensitivity of a Fabry-Perot pressure sensor were modeled and evaluated. Various factors, including variation in the effective diaphragm stiffness owing to stress-stiffening effect and variation in the optical path length are incorporated in the modeling. Analytical expressions were presented to analyze the stress reduction of the SDCD decoupling zone. Significant stress-release effect of a SDCD and the resultant low thermal cross-sensitivity of a SDCD pressure sensor were validated. It has been demonstrated that by replacing a conventional flat diaphragm with a SDCD in a Fabry-Perot pressure sensor, the simulated thermal cross-sensitivity can be reduced by about 10 times.

IV. Fabrication and characterization of the prototype of the proposed Fabry-Perot pressure sensor

A prototype of the designed Fabry-Perot pressure sensor with the proposed SDCD has been successfully fabricated. Optical measurement of wavelength-dependent transmittance of the fabricated Fabry-Perot pressure sensor was first performed and good agreement has been achieved between the measured and the simulated results, indicating that the design and optimization are both convincing and promising. Subsequently, a novel thermally incorporated Fabry-Perot pressure cell has been designed and combined existing optical measurement setup to extensively characterize the proposed Fabry-Perot pressure sensor. Reasonable agreements were obtained and significant reduction in thermal cross-sensitivity were demonstrated.

7.2 Recommendations

Due to the stress release effect of SDCD, it is believed that packaging induced stress of a Fabry-Perot pressure sensor using SDCD can also be significantly reduced. Therefore, it is possible that for the proposed devices to achieve desired performance without individual calibration, thereby reducing the unit cost of the device. However, further research effort must be made to investigate the various issues involved in the effective packaging and to model the packaging induced stresses for optical fiber extrinsic intensity-modulated Fabry-Perot sensors.

On the other hand, for monitoring and control applications, the sensors used have to function reliably over a period up to several years or hundreds of millions pressure cycles. Therefore, reliability modeling and experimental characterization need to be investigated to achieve long operational life of optical fiber extrinsic intensity-modulated Fabry-Perot pressure sensors for harsh environmental applications. Finally, the applications of the proposed SDCD structure have only been investigated for Fabry-

Perot optical fiber pressure sensors and MEMS condenser microphones. Since the SDCCD has the advantages of both ultra-high mechanical sensitivity and excellent performance in optical sensing, it has the greatest potential to be further developed as high-performance optical fiber microphone arrays.

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