

MEMS SCANNERS FOR SPECTROSCOPY AND LASER SCANNING SYSTEMS

by

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This is to certify that I have examined this copy of a master's thesis by

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ABSTRACT

Two novel methods are developed for Fourier transform spectroscopy (FTS) and laser scanned display and imaging based on electrostatic comb actuated micro-electro-mechanical system (MEMS) components. Construction of fully functional system demonstrators, in addition to the design, fabrication and characterization of novel optical MEMS components is also presented.

Infrared (IR) absorption spectroscopy is an established method for the detection and analysis of chemical and biological samples, and used extensively in a wide range of industrial and research oriented applications. Fourier transform spectroscopy (FTS) is one of the numerous IR spectroscopy techniques, distinguished by its unprecedented spectral discrimination paired with the inherent sensitivity, due to its throughput and multiplex advantages providing higher SNR and speed compared to other conventional methods like grating or Fabry-Perot spectrometers. The FTS method developed in this thesis employs a micromachined out-of-plane resonant mode comb-actuator simultaneously utilized for actuation, light dispersion, and optical path difference monitoring. The fundamental advantages offered by the proposed MEMS spectrometer system over the existing Fourier spectrometers are simplicity and compactness, good light efficiency (wide clear aperture), ease of fabrication and straightforward system integration capability.

Laser scanning is a key technology behind many modern high resolution display and imaging systems. Through the single source-single detector approach, laser scanning can significantly reduce the cost and the complexity of demanding applications, which traditionally employ costly detector arrays, and inefficient flood illuminators. Furthermore, fields like medical imaging diagnostics and measurement technology have increasing demand on compact and high performance imagers. Hence, miniaturization of high resolution scanning systems is crucial. A novel microlens array based high-resolution laser scanning architecture and its implementation using comb actuated MEMS translation stages are proposed. The scanning architecture consists of four cascaded micro-optical elements (1 microlens

and 3 microlens arrays (MLA)), where the lens and one of the MLAs are laterally moving. The novel device proposed here will enable precision alignment and scanning of the micro-optical components through integrated fabrication of polymer micro-optics on silicon micro-mechanics.

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NOMENCLATURE

ASE	Advanced Silicon Etching
CMOS	Complementary Metal-Oxide-Semiconductor
D/F/M/MLA	Double/Field/Mono MicroLens Array
DRIE	Deep Reactive Ion Etching
(E)OCT	(Endoscopic) Optical Coherence Tomography
FEA	Finite Element Analysis
FR4	Fire Resistant 4
FTIR	Fourier Transform Infra-Red (Spectroscopy)
FTS	Fourier Transform Spectroscopy
FWHM	Full Width at Half Maximum
IPMS	Institute for Photonic Microsystems
LGI	Lamellar Grating Interferometer
M(O)EMS	Micro-(Opto)-Electro-Mechanical Systems
MTF	Modulation Transfer Function
N/M/F/IR	Near/Mid/Far Infra-Red
OPD	Optical Path Difference
PCB	Printed Circuit Board
PSL	Pre-Scan Lens
PSF	Point Spread Function
RIE	Reactive Ion Etching
S/Q/W/VGA	Super/Quarter/Wide Video Graphics Array
SEM	Scanning Electron Microscope
SOI	Silicon-On-Insulator
SXGA	Super Extended Graphics Array

Chapter 1

INTRODUCTION

Micro-Opto-Electro-Mechanical Systems (MOEMS) is an emerging technology enabling functional manipulation, sensing, or analysis of light on a very small scale using precisely machined micromechanical devices. Spatial, spectral or temporal modulation and sensing of light beams can be performed with a speed and accuracy unmatched by the conventional systems. Moreover, MOEMS facilitates the observation and exploitation of some fundamental physical phenomena, such as subwavelength diffraction or wavelength selection, which were historically limited to millimeter or longer waves, due to the lack of high precision fabrication capabilities. Through such microfabrication technologies, alongside the development of lower-cost and faster opto-electronic devices, MOEMS offer great potential for integrated and miniaturized smart sensor systems. Today, with growing academic interest and industrial investments, MOEMS technology is becoming an established engineering field, whose major impact is soon to be seen in numerous fields, such as telecommunications, display and imaging systems, medicine, optical lithography, and spectroscopy.

At the heart of any MEMS component lays the transducer: the energy conversion mechanism translating a certain form of energy into mechanical motion. Conventional MEMS technology offers a wide variety of actuation methods, including but not limited to electrostatic, electromagnetic, thermal, and piezoelectric. Electrostatic (capacitive) actuation is arguably the most common of the bunch, due to its low power consumption, integrated nature, and compactness. Fundamental mechanism behind electrostatic actuation is the capacitive attraction forces between two charged plates separated by a dielectric layer, which is usually air in MEMS devices. The magnitude of the produced actuation force (or torque) depends on the surface area of the interacting surfaces. Comb actuators, consisting of two interdigitated finger sets of different potential, increase the interaction area substantially; thus, very high electrostatic force density can be extracted from a small area, enabling the

development of compact MEMS components with significant stroke.

This thesis presents two novel comb-actuated MOEMS solutions for near and far infrared (NIR/MIR) Fourier transform spectroscopy and refractive laser scanned imaging, both of which employ electrostatic comb actuated scanning platforms. The main focus is on various aspects of building the functional system demonstrators; however detailed treatments of the design, fabrication and characterization of the novel optical MEMS components also constitute a major part of the material.

Infrared (IR) absorption spectroscopy is an established method for the detection and analysis of chemical and biological samples, and used extensively in a wide range of industrial and research oriented applications. Fourier transform spectroscopy (FTS) is one of the numerous IR spectroscopy techniques, distinguished by its unprecedented spectral discrimination paired with the inherent sensitivity, thanks to the throughput and multiplex advantages providing higher SNR and speed compared to other conventional methods like grating or Fabry-Perot spectrometers. Despite the major decrease in size and increase in integrated software complexity in the last 30 years, these are still large instruments consisting of individual opto-mechanical components, optics, sensors and processing electronics manually assembled in a traditional manner. Furthermore, the output of these spectrometers consists of spectra, which are interpreted either by individuals or by specifically developed algorithms, significantly reducing the throughput. Hence, Fourier transform infrared (FTIR) spectrometers are generally used when size and cost of the equipment are of secondary importance compared to the necessary performance. In principle, FTIR spectrometers could potentially be used as compact and portable sensors or analyzers, but current instrumentation, particularly the scanning mirror mechanisms, do not (yet) fulfill the requirements of a small and easy to use sensor. Such a compact, portable, and real-time operating analyzer could be used for monitoring the quality of gasoline at gas stations, the quality and consistency of products (e.g. food and drug industry), the safety in fermentation environment (CO₂), and countless other out-of-the lab applications. A major part of this thesis work focuses on the design, integration, and characterization of a novel miniature FT-IR spectrometer with a comb actuated micro-electro-mechanical component constituting its basic building block. The MEMS component utilizes vertical resonant comb drives for actuation, light dispersion, and optical path difference monitoring. This is a novel platform for Fourier

spectroscopy developed within this thesis work and subject to European patent EP1677086. The patent is now licensed by Fraunhofer IPMS, Germany. The fundamental advantages offered by the developed spectrometer system over the existing Fourier spectrometers are high light efficiency (wide clear aperture), mechanical robustness, compactness, ease of fabrication and high integration level. A full FTS system can simply be implemented using the comb-actuated MEMS device and a single detector. Such a full system demonstrator is built and presented here with an opto-electronic position feedback mechanism integrated to the system for spatially-uniform/temporally-non uniform sampling of interferograms in Fourier spectrometers. Some alternative FTS architectures are also developed and demonstrated.

Laser scanning is the key technology behind many modern display and imaging systems. Barcode readers, laser printers, near-eye displays, document scanners, endoscopic imagers, and optical switches are some of the demanding applications employing various types of laser beam scanners. Fast and high precision scanning capabilities of MOEMS components significantly improves the performance of such systems in various aspects. High scanning frequency enhances the data throughput (refresh rate), large angular displacements leads to higher resolution, and precise and repeatable scanning improves the sharpness of the displayed or scanned patterns. Array or single micromirrors have dominated the field of MEMS display and imaging systems successfully for more than a decade, becoming one of the prime MEMS components to be employed in commercial products (Texas Instrument DLP [1], Fraunhofer IPMS' micromirrors [2] and SLM [3], Microvision Nomad [4], etc.) Due to the reflective nature of the beam steering mechanism, micromirror based scanning systems feature low optical aberrations (diffraction limited performance), wide clear aperture and straightforward optics. However, miniaturization of reflective systems is only possible to a certain extent, due to the folding of the optical beam after reflection from the mirror. This is a major drawback for applications like endoscopic imagers or micro-projectors, for which scanning-head size is a critical design issue. Efforts to build compact and folded in-line reflective scanning systems using MEMS microscanners resulted in overly complex systems, which are difficult and costly to implement. The refractive scanning method, on the other hand, uses laterally displaced lenses to steer a laser beam without any reflection, and allow compact, in-line scanning systems. In order to obtain considerable scan angles, lens displacements should be comparable to the lens diameter. However, moving bulky lenses

fast and precisely over a long range is challenging; so that refractive systems have limited scanning speed and image resolution. Using microlens arrays (MLA) instead of scanning mirrors or monolithic lenses has the potential of combining the advantages of reflective and refractive approaches. In the simplest configuration, two MLAs separated by a distance of two focal lengths can be used as a beam steering system, by moving one of the MLAs laterally. To obtain maximum steering, it is sufficient to move the entire array by half the lenslet size, which is generally at least one order of magnitude less than the full clear aperture size. However, imaging or beam steering systems employing a periodic array of microlenses suffer from diffraction problems resulting from destructive or constructive interference of the beam segments on the image plane. Due to the periodicity of the microlens array, the diffraction profile at the image plane is discrete; and only specific angles can be properly addressed. This limits the system resolution significantly. A solution to this problem, while maintaining all the advantages of MLA scanning is developed within the framework of this thesis project. The discrete addressing limitation is overcome by a novel approach, in which a prescan lens (PSL) is placed before the cascaded MLAs, and continuous addressing of the image plane is demonstrated. For fast scanning, a MOEMS micro-optical scanning platform is designed, fabricated, and characterized. The platform makes use of mechanical coupling phenomenon to achieve high frequency and long range scanning. A novel microfabrication process was also developed for monolithic integration of molded microlens arrays on MEMS structures.

1.1 Contributions of the Thesis

Following are the major contributions of this thesis research:

- *Conception, Design, and Development of a Micro-machined Spectrometer System* where the micro-mechanical element simultaneously utilizes the vertical resonant comb fingers for actuation, light dispersion, and optical path difference monitoring. This is a novel platform for Fourier spectroscopy and subject to European patent EP1677086 with Dr. Hakan Urey and alar Ataman as inventors. The patent is now licensed by Fraunhofer IPMS, Germany.
- *FTS Full System Demonstration including Optical position feedback.* Full spectrometer

system demonstration for both narrowband and wideband spectrum measurements in the visible, and NIR are performed. The system is well-suited for mid and far IR systems. Alternative FTS architectures and non-MEMS polymer scanners are developed and efforts are under way for a number of different demonstrators. An opto-electronic position feedback system is integrated to the system for spatially-uniform/temporally-non-uniform sampling of interferograms in Fourier spectrometers

- *Design and Development of Integrated Microlens Array/MEMS Stage:* A micro-mechanical one-dimensional translation stage bearing with monolithically integrated molded micro-optical components (microlenses and microlens arrays) are fabricated under TÜBITAK grant 106E068 in collaboration with Chalmers Institute of Technology, Sweden.
- *Development of 1D Scanned Imaging system and Control Electronics:* A high resolution compact laser scanner system for endoscopic imaging based on cascaded movable microlens arrays. The system offers high resolution with a special actuation waveform and drive signals. .

1.2 Outline

This thesis covers the work done within two main projects, both of which employ comb actuated MOEMS scanning components. The literature on MEMS spectroscopy and laser scanning devices is very rich. A technological overview for such systems with detailed information on a handful of state-of-the-art devices constitute the content of Chapter 2. Chapter 3 is an introduction to the fundamental principles of electrostatic actuation, which a special emphasis on the electromechanics of comb actuation. The basic principles established within this chapter are heavily referenced by the following two chapters. The FTIR related work is treated in Chapter 4. This chapters begins with an extensive review of Fourier transform spectroscopy, and then presents the design, fabrication, and characterization of the novel MEMS FTS device. Furthermore, some fundamental aspects of FTS system integration are also analyzed. The last section of this chapter is shifts the focus slightly, by presenting two FTIR configurations developed based on the novel polymer (FR4) platform. Chapter 5 is the other major chapter that focuses on laser scanning. The first of the three sections of this chapter is a brief review of laser scanning technology, and introduces some

crucial concepts important in scanning system design. Analysis of the novel MLA scanning system is given in Section 5.2. Last section in this chapter is about the design, fabrication, and characterization of the micro-optical scanner developed for this application. Lastly, Chapter 6 includes concluding remarks and a section on future work in this area.

Chapter 2

STATE OF THE ART

2.1 Miniaturized Fourier Spectrometers

Conventional Michelson interferometer based FTIR systems rely on precise but complex mirror drives, which constitutes a considerable part of the entire system costs. Such systems are often inapplicable for industrial applications because of their sensitivity to shock and vibration, large size and low measuring speed, not qualified for time resolved FTIR-spectroscopy. Furthermore, conventional IR spectrometers are often too expensive for industrial applications. Miniaturization and durability in harsh environments are the main challenges for the development of optical MEMS based spectrometers. Compared to conventional systems, the use of optical MEMS opens furthermore the possibility to produce small and reliable systems in a large quantity at admissible production costs. Several MOEMS devices have been developed for miniaturized spectrometer demonstrators targeted mostly the visible/near-IR part of the spectrum. Although using visible light requires higher precision of the opto-mechanical components, it avoids the complications of IR optics and atmospheric absorption. Extension into the IR region is straightforward, requiring the use of IR optics and reflective mirror coatings.

2.1.1 Michelson Interferometers

Majority of the miniaturized FTS systems in the literature share the Michelson interferometer layout, in which a beam splitter, a fixed mirror and a MEMS movable mirror are fabricated or assembled on top of a common platform. Sin et al, have developed a miniaturized visible/NIR Michelson interferometer assembled on a micro-optical bench fabricated using Deep RIE (Reactive Ion Etching) process on a SOI (Silicon on Insulator) wafer [5]. The interferometer components lie perpendicularly to the bench surface, so that the light path is parallel to it. The DRIE etched sockets are female mechanical flexure structures

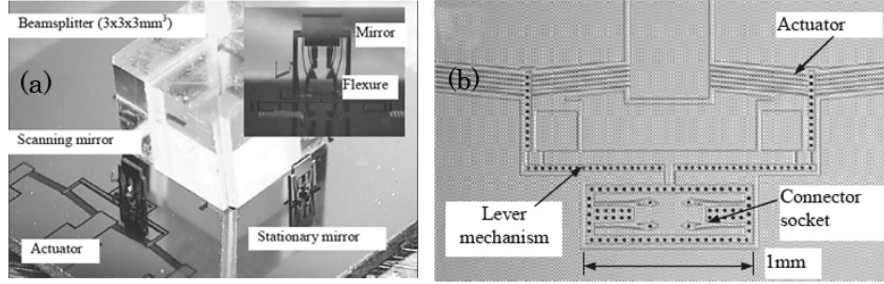


Figure 2.1: Michelson type Fourier transform spectrometer developed by Sin. Et al. [5] (a) Assembled microspectrometer (b) The electro-thermal actuator with a lever mechanism.

that allow a snap-fit assembly with micromachined silicon mirrors. The overall dimension of the FTS optical bench is 1 cm^2 , and its embedded thermal actuator has a couple of V-beam structures whose beam length is 1 mm . The mirrors are DRIE micromachined structures with reflection area $500 \times 450 \text{ } \mu\text{m}^2$ and $750 \text{ } \mu\text{m}$ long flexure structures for pick-and-place assembly, as shown in Figure 2.1. An electro-thermal in-plane actuator with stroke amplification mechanism provides the amplified scanning motion for the moving mirror. The thermal actuator generates a maximum translation of $30 \text{ } \mu\text{m}$ at 22 V input, corresponding to a theoretical resolution of 330 cm^{-1} . Main drawbacks of this system are the cumbersome assembly process creating significant misalignments especially in the vertical direction, fabrication difficulties, and low travel range of the thermal actuator. However, high level of integration and compactness of the overall system are major advantages.

Kenda et al. has implemented a similar system for mid-IR range using the Michelson interferometer with the vertical comb actuated MEMS micromirror developed at Fraunhofer IPMS shown in Figure 2.2a [6]. The system is manually assembled on top of a common platform without any snap-fit mechanism. The scanning micromirror is operated in vacuum, due to the limiting effect of viscous air damping on achievable stroke length. An optical position feedback system providing precise monitoring of the mirror position is integrated into the system together with interferogram sampling, data acquisition and processing electronics. All system components are assembled in a compact package, constituting a successful demonstrator. The MOEMS micromirrors are manufactured in a CMOS compatible process on a SOI substrate. The device consists of a metallized actuator plate with an area of

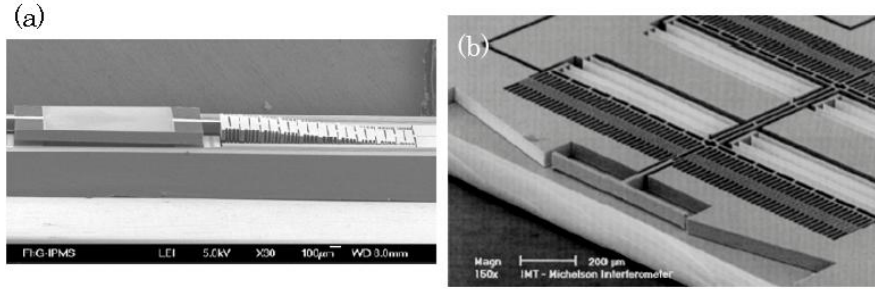


Figure 2.2: SEM photographs of translatable MOEMS devices. a) Electrostatic vertical comb actuated translatable micromirror with pantograph-like suspensions developed by Fraunhofer IPMS [6]. The circular mirror area is 1.65 mm^2 . (b) Electrostatic lateral comb actuated translatable micromirror developed by IMT. The mirror dimensions are $75 \mu\text{m} \times 500 \mu\text{m}$ [7].

1.65 mm^2 acting as mirror, bearing springs and electrodes for the electrostatic drive. Due to high force density produced by the comb actuators, $200 \mu\text{m}$ translatable displacement could be achieved with a driving voltage lower than 40 V at an ambient pressure below 500 Pa . The actuator operates at a resonant frequency of 5 kHz . As a result of this mechanical performance, a maximum spectral resolution of 25 cm^{-1} at acquisition time of $200 \mu\text{s}$ per spectrum is achieved. Large mirror displacement, wide mirror aperture and integrated actuator structure are significant achievements making the system a versatile spectrometer with good performance, despite the complications of vacuum packaging, and stringent precision requirements for assembly. A similar spectrometer concept was introduced by Manzardo, in which the movable mirror was implemented on the side wall of the device layer of an SOI wafer and actuated with in-plane comb actuators [7]. The MOEMS device has been fabricated by surface near SOI technology, where the structure sidewalls are used as reflective mirrors (Figure 2.2a). A maximum optical path difference of $77 \mu\text{m}$ was obtained with the lateral comb actuators. Light collection efficiency of the device is low, since the mirror area is limited by the thickness of the SOI-layer of typically $75 \mu\text{m}$. Efficient use of the mirror area imposes the use of anamorphic optics and optimization of DRIE etching process is critical to ensure a uniform and perpendicular mirror surface.

Interferometric sensors are extremely sensitive to misalignments. Individual fabrication and manual assembly of constituent components can potentially cause alignment imperfections that can degrade the sensor performance. A method to avoid such a problem is the

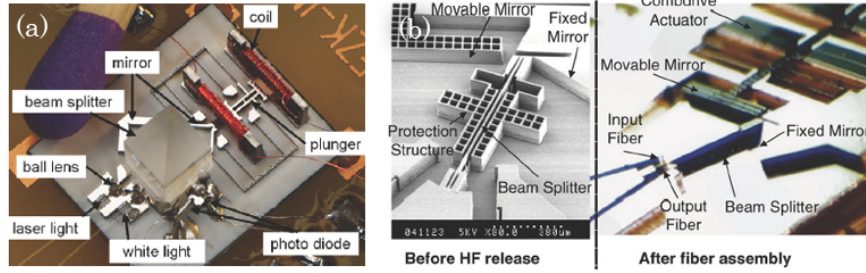


Figure 2.3: Integrated micro-spectrometers (a) Michelson IR spectrometer on a NiFe optical mini-bench with LIGA-fabricated electromagnetic actuator for the displacement of the mirror [8]. (b) Microscope SEM images of a fully integrated Michelson spectrometer showing details of the beam splitter fixed and movable micromirrors, and fiber U-grooves. Sidewalls obtained by KOH etching[9].

integrated microfabrication of the optical components and the subsequent assembly on a common optical platform. One such system was developed by Solf [8] by fabricating the passive components and the electromagnetic actuator on a $380\text{ }\mu\text{m}$ thick electroplated NiFe platform using LIGA technology. The spectrometer is shown in Figure 2.3 and includes an $11.5 \times 9.4\text{ mm}^2$ electroplated optical mini-bench of an electroplated soft NiFe alloy. The same material is used for the electromagnetic actuator, which enables a displacement of the mirror over $110\text{ }\mu\text{m}$. A passively aligned silica ball lens is used for collimation and a InGaAs p-i-n photodiode for recording the interferogram. Assembly of the ball lens, the detector and the beamsplitter is done manually. A Michelson interferometer implemented entirely on an SOI wafer, which does not require any manual assembly was developed by K. Yu et. al. [9]. The FTS hardware, including the beam splitter, reference mirror, MEMS micromirror, and fiber U-grooves are simultaneously fabricated by a simple bulk micromachining process and integrated on a silicon optical bench occupying less than $4\text{ mm} \times 8\text{ mm} \times 0.6\text{ mm}$ (Figure 2.3). This integrated Fourier transform spectrometer has a measured spectral resolution of approximately 45 nm near 1500 nm wavelength. Although very compact, and relatively easy to fabricate, the resolving power of the spectrometer is low, because of the poor optical properties of the silicon components, and relatively short mirror translation.

2.1.2 Lamellar Grating Interferometers

An alternative approach to Fourier transform spectroscopy utilizes variable depth binary diffraction gratings as the interferometer engine, and has recently become an attractive choice for miniaturized NIR spectrometers. Named by their inventors Strong and Vanasse [10], lamellar grating interferometry requires fewer optical components compared to the Michelson interferometer, and greatly facilitates the design and implementation of spectrometer systems. Operation principle is identical to the Michelson interferometry, but the interferogram is recorded by a detector at the 0th diffraction order. The variable depth diffraction grating should provide a sufficiently large travel range for high resolution and a small grating period for elimination of the cross-talk between diffraction orders. MOEMS technology offers very precise and high volume fabrication capabilities for such gratings. A number of different MEMS based LGI systems have been developed in recent years.

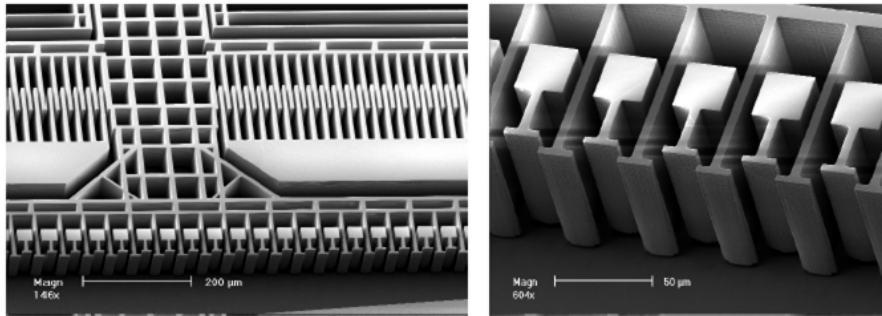


Figure 2.4: SEM photographs of Manzardo's lamellar grating interferometer device [11]

Manzardo has demonstrated a translatable grating device driven by electrostatic in-plane comb actuators used for optical path modulation in miniaturized FTIR spectrometers. [11]. The fabrication technology and the actuation mechanism are same as the translatable mirror device developed by the same group [7]; hence the fundamental limitations of said system are also present here. The device has a maximum optical path difference (OPD) of 0.5 mm, leading to a measured spectral resolution of 20 cm^{-1} . This is an impressive performance figure, proving the potential of lamellar grating interferometry for miniaturized spectroscopy. The wavelength range of the system extends from 350 nm to 2600 nm and is limited by type of the photodiode (PD) detector. The wavelength accuracy is demonstrated to be better

than 0.1%. This technology has recently been commercialized to be the first MEMS based commercial fourier transform spectrometer [12].

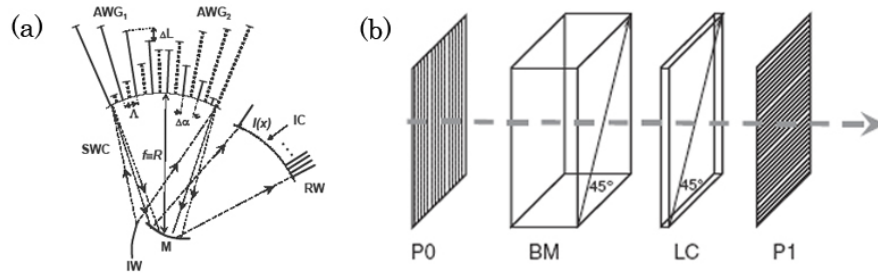


Figure 2.5: Two novel stationary Fourier transform spectrometers (a) Schematic of the FT AWG microspectrometer with two interleaved truncated arrayed waveguide gratings (AWGs) [13]. Dotted and dashed lines in the slab waveguide combiners (SWC) output direction represent light beams from AWG1 and AWG2, respectively. IW: input waveguide; SWC: slab waveguide combiner; M: mirror; RW: receiver waveguides; IC: image curve. (b) Schematic diagram of single stage in LC-controlled FOSA. P0 and P1 denote crossed polarizers and BM is a 45° -oriented birefringent material [14].

2.1.3 Stationary Fourier Spectrometers

Miniaturized spectrometers are primarily targeted for industrial and field applications; hence, mechanical robustness is a critical issue. Fourier transform spectrometers without moving parts have the advantage of avoiding the design complications imposed by stringent mechanical reliability requirements on scanning elements. The straightforward approach to this problem has been in the form of a Michelson interferometer with one or both mirrors tilted, such that the incident wavefront on the tilted mirror/s is subjected to a linearly changing phase modulation. In such a configuration, an interferogram can be recorded on the interference arm of the beamsplitter with a detector array. The lack of complex mirror drive systems enables the use of relatively large mirrors that can introduce significant end-to-end optical path difference when tilted, enhancing the spectral resolution. Versatility of this approach has been proven by several bulk [15] or millimeter-scale [11] spectrometers designed for visible/near-IR spectral bands. An arrayed waveguide grating (AWG) device, further avoiding the alignment problems of Michelson interferometers, was proposed

by Cheben [13]. The device is fabricated on an SOI wafer and employs two interleaved truncated AWGs. Interference fringes with a wavelength dependent spacing are produced on the diffraction plane, where the output wavefronts W1 and W2, originating in the two interleaved dispersive elements, overlap with an inter-wavefront angle. The light spectrum is retrieved by Fourier transformation of the measured interferogram. Each AWG consists of 255 waveguides (hence, a total of 510 interleaved waveguides) with a constant length increment of $8.6 \mu\text{m}$ and the waveguide pitch $7.8 \mu\text{m}$. This corresponds to a theoretical diffraction limited resolution of 0.07 nm at 1500 nm . The focal length of the slab waveguide combiner is 4 mm . The interferogram is spatially sampled at 960 points, which corresponds to a receiver waveguide pitch of $2 \mu\text{m}$ and recorded with a charge coupled device (CCD). Lu et al. has developed a liquid-crystal (LC)-based Fourier spectrum analyzer [14]. The system consists of a birefringent filter array with an embedded LC phase modulator. The LC phase modulator is used to control the phase difference between two orthogonally polarized beams to avoid mechanical movement in conventional Fourier transform spectrometers. Unlike normal spectrometers, the FOSA retrieves the spectrum's Fourier coefficients instead of performing direct spectrum measurements by controlling the phase retardation of a LC phase modulator. Interferogram recording in all types of stationary Fourier spectrometers predicate on large detector arrays, which are widely available for a low cost in the visible band. However, such arrays are extremely expensive in IR range, within which stationary Fourier spectrometers become impractical.

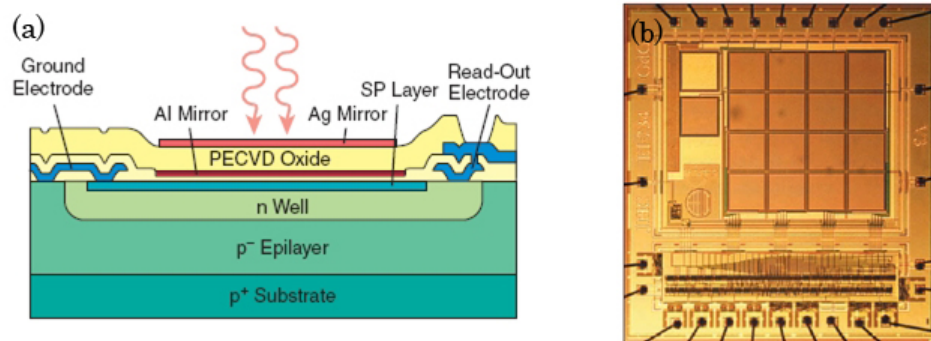


Figure 2.6: (a) A cross section drawing of a single channel Fabry-Perot cavity of the integrated bulk micromachined Fabry-Perot interferometer developed by Wolffenbuttel [16]. (b) Microscope picture of the fabricated spectrometer chip.

2.1.4 Non-FTS systems

The literature on miniaturized spectrometers is not limited to Fourier transform spectroscopy; on the contrary, majority of the developed systems exploit alternative approaches. In order to illustrate how broad and established the field is, a few of those systems with remarkable features - performance, fabrication, or design wise - are briefly introduced here. A CMOS integrated Fabry-Perot spectrometer was developed by Correia, et al. [17]. The system consists of 16 micro-fabricated Fabry-Perot cavities optimized to operate in different wavelength ranges within the visible band; though the operation range can be extended into the IR region with modifications in the cavity parameters and the detectors. The device features an impressive level of integration with embedded electronics and signal processing, and the microfabrication process is remarkably sophisticated. However, low spectral selectivity fails to justify the overall complexity of the system. Kenda et al. demonstrated a scanning spectrometer based on a rotating grating fabricated by Fraunhofer IPMS [19] on an SOI wafer with special crystal orientation where a blazed grating is etched on the top surface of a torsional MEMS scanner [18]. A photodetector placed at the first diffraction order records the time-resolved spectrum of the incident light as the grating is rotated by the MEMS micromirror. Due to large aperture size of grating (9 mm^2), the spectrometer system offers good light efficiency and spectral performance (10 nm FWHM resolution at 600 nm) but fabrication requires special wafers and a compromise between the detector size and the spectral resolution limits further enhancement of resolving power while maintaining an acceptable efficiency. Furthermore, due to the operation principle, the free spectral range of the spectrometer is also limited due to the finite angular spacing between diffraction orders. This limitation is a common problem for all grating spectrometers, and is eliminated in Fourier transform spectroscopy. Another scanning spectrometer with a grating perpendicularly assembled on a radial comb actuated MEMS platform is developed by Heaton et al. [20]. While being a very neat design overall, the system suffers from cumbersome assembly, low fabrication yield and delicate mechanics. Other notable miniaturized spectroscopy methods include arrayed waveguide spectrometers [13] and tunable optical filters [21].

An elegant but relatively low resolution MEMS spectrometer was developed by Kung based on a variable length optical cavity. The system is a Fourier transform spectrometer with a linear architecture that works by sampling a standing wave form within the cavity.

Constituting components are an electrostatically actuated MEMS mirror with on-resonance displacement of up to $65\ \mu\text{m}$, a thin-film photodetector, and an electrical back plane for actuating the mirror. The integrated device offers mirror stability and fixed relative alignment of the three components. The spectrometer has better than $32\ \text{nm}$ resolution at $633\ \text{nm}$.

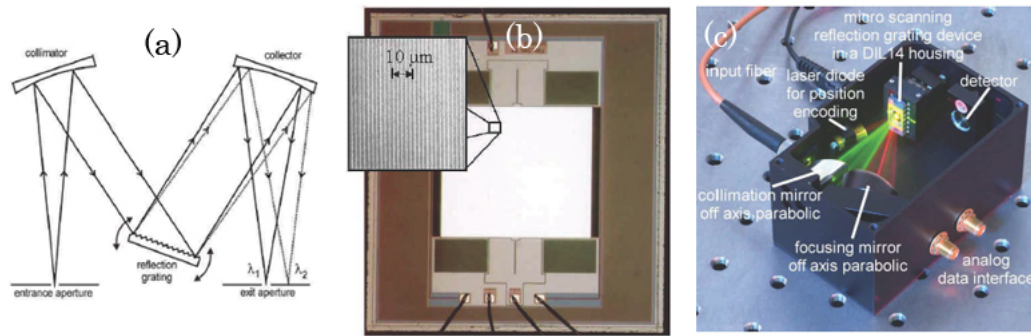


Figure 2.7: (a) Schematic of the optical setup of a classical Czerny-Turner grating monochromator (b) A micrograph of the torsional platform device carrying a diffraction grating. (c) Packaged microspectrometer demonstrator [18].

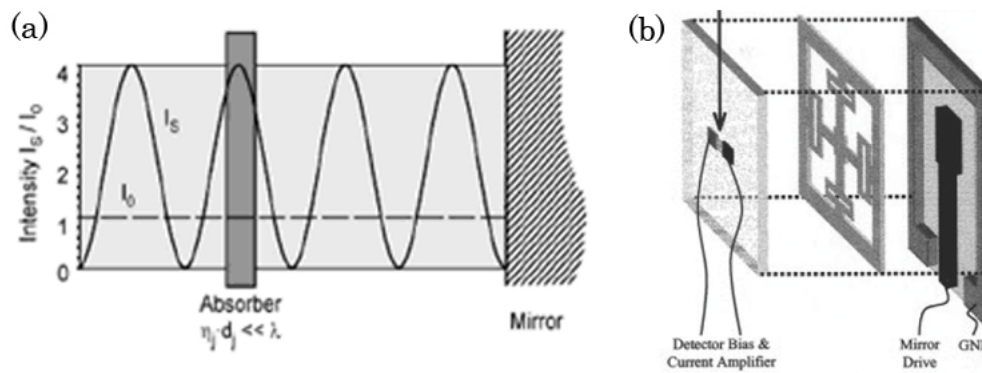


Figure 2.8: (a) Schematic of a standing-wave spectrometer and light intensity pattern where d is the thickness of the detector and z is the distance from the mirror to the detector. (b) Integrated standing-wave spectrometer. [22]

2.2 MEMS Based Miniaturized Display and Imaging Systems

There is a gradually growing demand for ultra-miniaturized display and imaging engines both from the high volume consumer electronics (hand-held projectors, wearable displays) [23] and specialized medical imaging (endoscopic OCT and imaging) markets [24]. However, packing a fully functional display or imaging system within a few millimeter cube volume is a challenging task. Conventional choices for such systems are CCD/CMOS 2D sensors or fiber-optic bundles for collecting, and matrix addressable displays (Liquid Crystal on Silicon (LCoS), Micromirror Arrays) for viewing images. These can perform reasonably well in terms of image quality and resolution; but they are not adaptable for extreme miniaturization due to their array nature. Imaging through 2D scanning of a single laser beam requires a single point source and a single detector; hence, the overall system size can be significantly smaller. Furthermore, lasers can produce monochromatic, coherent, small and collimated beams, boosting the quality of projected or acquired images in terms of brightness, contrast, and color reproduction. However, the scanning approach has two fundamental challenges:

- *Scanning speed:* Acquisition or display of the image is accomplished pixel-by-pixel; pixel count/refresh rate and available system bandwidth (both mechanical and electrical) are strongly antagonistic. Image quality standards impose hard-to-meet mechanical requirements on the scanning element. For instance, for an SVGA display, the fast axis of the scanning element should oscillate at least at 16 KHz.
- *Light source modulation:* The light source should be modulated at a very high rate, unlike the matrix addressable displays or detector arrays for which flood illumination is sufficient.

With the help of MEMS scanning elements, fast lasers/LEDs, and processing power of modern integrated circuits, these challenges can be overcome. A review of such state of the art systems constitutes the basis of this section.

Laser scanning can either be reflective or refractive, depending on the geometry of the optical path of the beam. Reflective systems utilize rotating mirrors to control the deflection angle of the incident laser beam. Diffraction limited performance, spot uniformity along

the image plane, and simplicity of the required optics are the major advantages of this method. MEMS scanning mirror is a device with a tiny mirror at its center. This mirror is connected to small flexures allowing it to rotate vertically and /or horizontally to capture (imaging) or reproduce (display) an image pixel-by-pixel. MEMS scanning micromirrors are the most common choice for ultra-small yet high resolution display and imaging systems. The literature on microscanner technology is very rich with devices targeting endoscopic coherence tomography, hand-held projectors, head mounted displays, and various other applications. A comprehensive review of scanning micromirror technology is out of the scope of this work; hence we will only focus on ultra- miniaturized microscanner based scanning systems. However, a comparative chart with state-of-the art scanning mirrors is given in Figure 2.9.

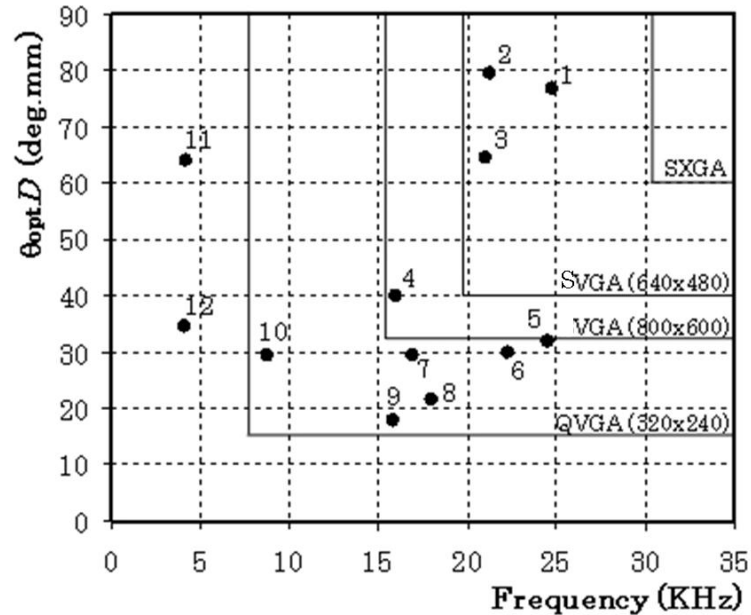


Figure 2.9: Performance comparison for the state of the art MEMS microscanners. The quantitative figure of merit for microscanner performance was defined by Urey [25], $\theta_{opt}D$ (maximum optical scan angle times the aperture size of the scanning element) product defines the number of available horizontal pixels, scan frequency determines the refresh rate of the system. (1-[26], 2-[27], 3-[28], 4-[29], 5-[30], 6-[31], 7-[32], 8-[33], 9-[34], 10-[35], 11-[36], 12-[37])

2.2.1 Endoscopic Laser Cameras

Endoscopic laser scanning is a potential application for MEMS miniaturized scanning systems. Miyajami et al. from Olympus Corporation have developed an endoscopic laser scanning head with a 2D scanning micromirror [36]. Figure 2.10 shows a schematic drawing of this confocal microscopy system. The scanning head consists of an objective lens, an electrostatic 2-D MEMS scanner with a through hole, and the tip of an optical fiber, all packaged inside a thin aluminum tube. As the objective, a glass molded double sided aspherical lens is used to suppress spherical aberrations. A laser diode (LD) and a photomultiplier are employed as the optical source and the detector, respectively. The scanning system operates as follows: The beam diverges from the fiber, passes through the hole in the mirror is reflected back from a fixed mirror fabricated at the center of the aspherical lens. Then, the beam is scanned two-dimensionally by the MEMS scanner, and the scanned beam is focused on the sample by the objective lens. The reflected light is collected by the same optical system and directed on the photomultiplier by a fiber coupler. The driving signal of the MEMS scanner and the modulation signal for the laser diode are synchronously supplied from a computer, which also processes the photomultiplier output to construct the scanned image. In the horizontal direction, the driving frequency coincides with resonance of inner mirror frame and is over 3000 Hz. Outer mirror frame is responsible from vertical scanning and has a resonance frequency of 840 Hz. However, this axis is driven off-resonance at 20 Hz, and for achieving the required frame rate, the confocal image is displayed by interlacing. The opto-mechanical design of the system is very complex, but elegant. However, the performance of the MEMS scanner is low; and therefore, the resulting image resolution is limited to 150 x 150 pixels, while the pixel size is 0.5 μm (with 400nm imaging wavelength). There certainly is room for further enhancement of the system performance with an improved scanner.

Unlike the Olympus system, majority of the reported micromirror based endoscopic imagers are side looking, such that the optical path is folded by the reflection from the mirror. Such a configuration significantly reduces the complexity of the optics, but, at the same time, increases the overall system size. Papageorgas et al. have successfully developed such a system using a two one-dimensional scanning mirrors [38]. This system has been designed for color macro-endoscopy applications, where the field of view is in the order of

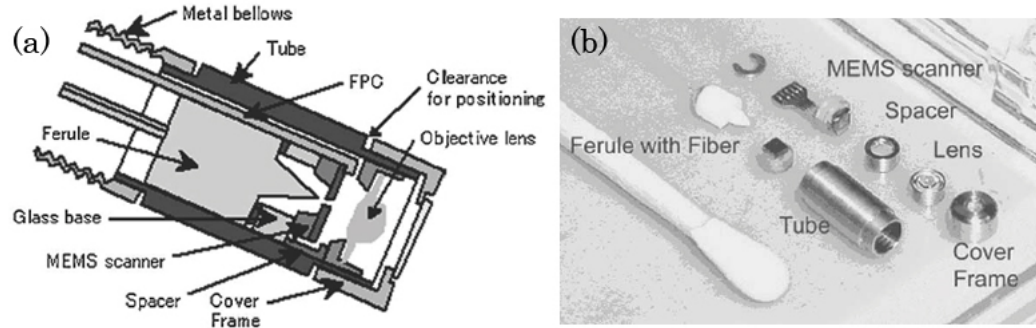


Figure 2.10: Endoscopic laser scanning system from Olympus Corp. (a) Cross-sectional view of the scanning head (b) Photograph of the scanning head dissected into its constituents [36].

10-20 mm², with a resolution of at least 50 μm and image rates in the order of ten frames per second. Figure 2.11a summarizes the scanning concept. The scanned beam is a composite laser (red, green and blue) color imaging. Individual micromirrors accomplish vertical and horizontal scanning. The first micromirror, used for horizontal scanning, has a size of 3 x 4 mm², and is resonantly driven at about 1.2 kHz. The vertical scanning mirror has a size of 4 x 4.8 mm² is driven with a saw-tooth waveform. Both mirrors are capable of mechanical deflections up to $\pm 3^\circ$ (12° optical scan angle). Entire scanning engine fits into a 11 mm diameter cylindrical package. The data acquisition, control, and processing system acquires, reconstructs, stores, and displays color images with resolutions of at least 500 x 500 pixels and at rates up to 20 frames/sec. The competent performance level of this system proves that, when miniaturization is not the primary goal (please note that the system is for macro-endoscopic applications), the overall system performance can significantly be enhanced.

Despite the obvious richness of literature on the reflective miniaturized scanning systems, the refractive approach is less common. The main reasons behind this lack of interest are some important challenges associated with this method, which mainly due to the difficulties in fast translation of bulky lenses and dispersive nature of refraction. Mostly due to these challenges, the scanning lens systems proposed in the literature are limited to low speed, and low resolution systems developed for fiber coupling [39] [41] or vertical cavity surface emitting laser (VCSEL) alignment [40]. However, there still have been numerous such

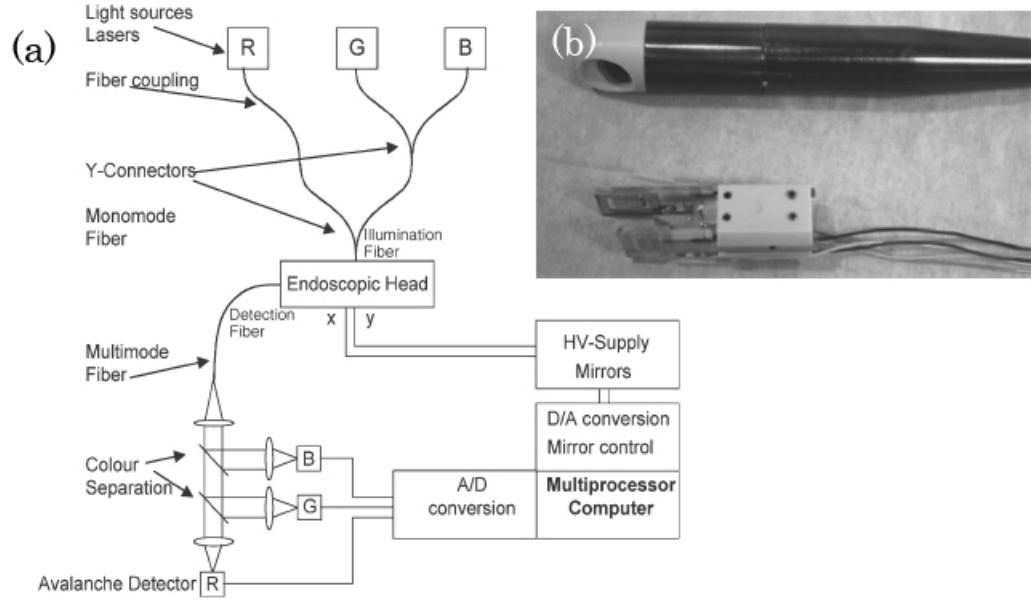


Figure 2.11: (a) System concept of the microscanning endoscope. (b) Outer and inner parts of the endoscope head. The inner part acts as a carrier for the MEMS mirrors, the imaging lenses, and the prism/beam splitter combination [38].

efforts. Jsin and Xie reported a microendoscopic confocal imaging probe based on a large-vertical-displacement microlens scanner [42]. Photoresist reflow technique is used to form a microlens on a lens holder that is integrated with a vertical microactuator, as shown in Figure 2.12a. The actuator structure is similar to the one shown in Figure 2.12; however, this device employs two thermal bimorph actuators to obtain a lateral translation of the platform. The device is only capable of axial scanning (depth scanning of the focal plane). With a footprint of 3 mm x 2 mm, the microlens scanner is reported to vertically displace the integrated polymer microlens by 0.7 mm at a dc voltage of 23 V. The microlens chip is directly packaged into a 5-mm-diameter imaging probe (Figure 2.12b). The architecture of the imaging probe is very simple and is just composed of this microlens, a graded-index (GRIN) rod lens, a metal mold, and a cover. Due to lack of transverse scanning, the field of view of the system is limited, but the large vertical displacement ensures a wide axial scanning range. Another limitation of the system is low scanning speed due to the slow response time of the thermal actuators, and weight of the lens.

A magnetically actuated MEMS scanner for endoscopic confocal imaging application

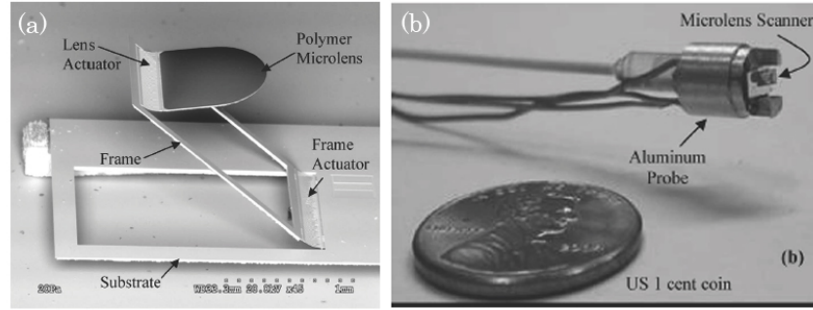


Figure 2.12: (a) SEM of a fabricated microlens scanner with integrated polymer microlens. (b) Packaged OCM endoscope. [42]

with a microfabricated ferromagnetic nickel platform and thermosetting polydimethylsiloxane (PDMS) microlens was demonstrated by Siu et al. [43]. The device, shown in Figure 2.13a, is driven by an external AC magnetic field, eliminating chip circuitry and thermal deformation from joule heating. The resonant frequency of 215.2 Hz and scanning angle of 23° of the scanner have been demonstrated. When compared to the state-of-the-art micromirror based scanning systems, the system performance is significantly worse, especially in terms of scanning frequency.

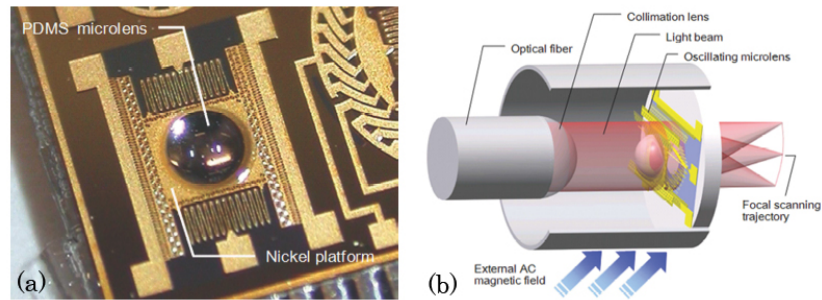


Figure 2.13: a) Microphotograph of a PDMS microlens with a ferromagnetic nickel platform (b) A magnetically actuated microlens assembled with an optical fiber provides focal

2.2.2 Endoscopic Optical Coherence Tomography Systems

Optical Coherence Tomography (OCT) is a powerful medical imaging method for sub-surface images of tissues based on low-coherence interferometry. In laser interferometry,

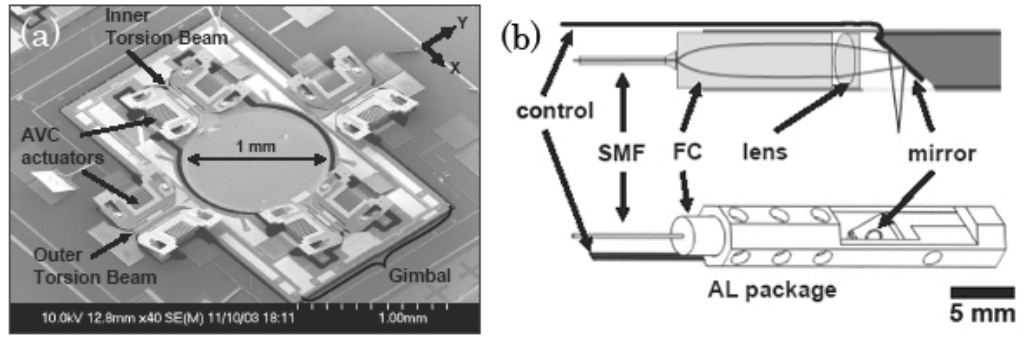


Figure 2.14: Endoscopic OCT (EOCT) system employing a two-axis scanning mirror developed by Aguirre and Piyawattanametha [45]. (a) SEM photo of the MEMS mirror. (b) Schematic drawing of the endoscopic scanning head.

interference of the split arms can still be observed within very large OPDs (please refer to Appendix A for a detailed discussion on the relation between spectral confinement and coherence length). In OCT, interference range is shortened to a distance of microns, due to the use of broadband light sources. Light with broad bandwidths can be generated by using superluminescent diodes or lasers with extremely short pulses [46]. Cross-section or sliced images of tissues is obtained by scanning an optical path difference between two interferometer arms (just like FTIR systems), one of which reflects back from the tissue, while recording the interferometric detector response, which gives reflectivity as function of optical depth (which is called the A-line). Due to the low coherence length of the broadband source, any light that is outside the short coherence length will not interfere, and will not be detected. It naturally follows that the axial resolution is equal to the coherence length of the broadband source. Transverse scanning, on the other hand (B-scan), is performed by external mechanical scanners, for which MEMS technology offers a great potential.

A high resolution three-dimensional endoscopic optical coherence tomography (EOCT) system employing a two-axis scanning mirror was developed by Aguirre and Piyawattanametha [45]. The 1 mm diameter MEMS mirror is utilized for transverse scanning and driven by angular vertical comb (AVC) actuators on both an inner mirror axis and an outer, orthogonal gimbal axis (Figure 2.14a). Using a differential drive scheme, a linearized position response over ± 6 degrees mechanical angle was obtained. Three-dimensional images (in one axial, two transverse dimensions) are then constructed from sequential depth

lines of backscatter by scanning the MEMS mirror. The flexible, fiber-optic probe head is smaller than 5 mm, and less than $4\text{ }\mu\text{m}$ axial (depth) and $12\text{ }\mu\text{m}$ transverse resolutions are demonstrated. A 1 mm diameter mirror and good angle response allowed imaging with a large number of resolvable spots. The large mirror aperture would allow for even shorter focal length to reduce transverse spot sizes to less than $5\text{ }\mu\text{m}$. This system is currently the best performing EOCT among such published system in terms of resolution.

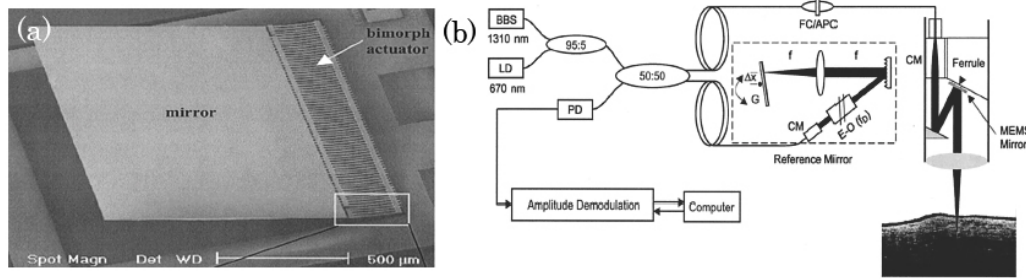


Figure 2.15: (a) Schematic of EOCT with a MEMS mirror developed by Xie. BBS, broadband light source; LD, aiming laser; PD, photodiode; CM, fiberoptic collimator. (b) Scanning-electron micrograph of the CMOSMEMS mirror [47].

A two dimensional EOCT system employing a thermally actuated MEMS microscanner for transverse (lateral) scanning was introduced by Xie et al. [47]. The bimorph actuators of the scanning mirror (Figure 2.15b) can produce an optical scan angle greater than 37° , exceeding the transverse scanning requirements for EOCT and confocal endoscopy [48]. Figure 2.15 depicts the schematic of the OCT system, and an SEM picture of the MEMS mirror. The MEMS mirror allows 4.3 mm two-dimensional OCT imaging at 5 frames/sec. The transverse resolution is $20\text{ }\mu\text{m}$.

Zara et al. has developed a pseudo-forward looking endoscopic using a linear MEMS actuator to tilt the mirror [49]. The integrated force array (IFA) actuators, is a network of hundreds of thousands of micrometer-scale deformable capacitors that synchronously contract, in the presence of a differential voltage applied across the capacitor electrodes. Figure 2.16b is a schematic of a portion of the cell network of the IFA. The small scale of the cells allows these devices to utilize the short-range electrostatic forces effectively by reducing the distance between the electrodes and also by allowing for large numbers of cells to contribute to forces and displacements. The optical scan angle exceeds 140° near the

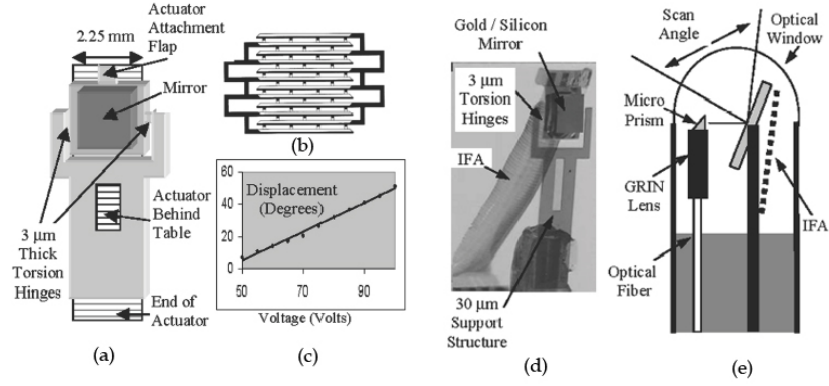


Figure 2.16: (a) Schematics of the fabricated scanner and a portion of the cells in the IFA electrostatic MEMS actuator. (b) MEMS scanning device: photograph of the device, and schematic of the scanner encased in the proposed endoscopic probe [49].

resonant frequency of the scanners and two-dimensional cross-sectional images covering an area of 4.2×2.8 mm -limited by scope size- and at 5 Hz refresh rate were acquired with the system assembled within an endoscopic probe.

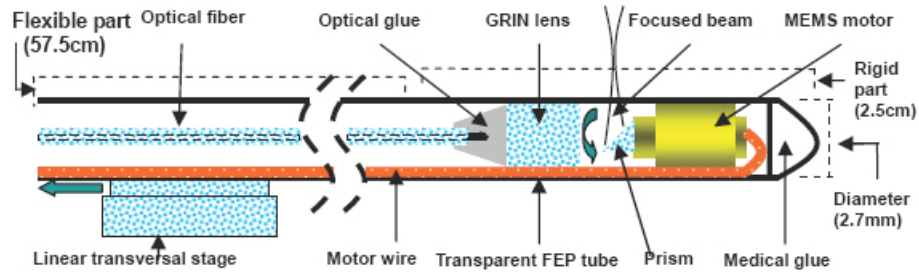


Figure 2.17: Rotational prism based endoscopic OCT system [50].

An interesting OCT system based on a MEMS motor rotating a prism was developed by Su et. al [50]. The novel probe was designed and constructed with a 1.9 mm diameter MEMS motor, and eliminates the need to couple the rotational energy from the proximal to the distal end of the probe. At the distal end, a prism mounted on a micromotor deflects the light rays to create a transverse circular-scanning pathway. The entire package was fitted inside a 2.7 mm tube, which is significantly smaller than other systems enlisted here. With an A-line scanning rate of 20 KHz, four-thousand A-lines were acquired within 0.2

sec, leading to a refresh rate of 5 frames/sec.

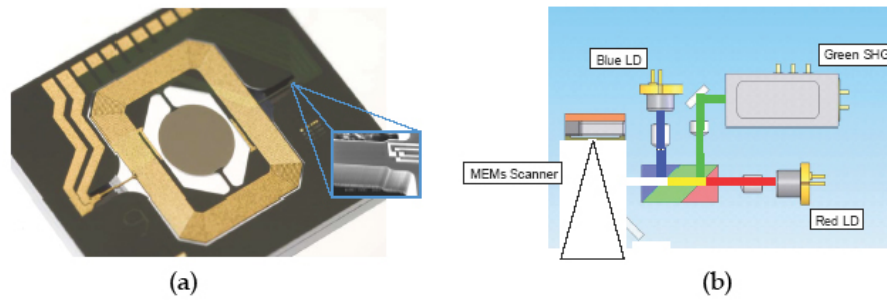


Figure 2.18: Pico-projector system developed by Microvision, Inc. (a) MEMS scanning mirror die and closeup of piezoresistive strain sensor used for vertical scan position feedback. (b) Scan engine subsystem with MEMS scanning mirror and laser light sources [53].

2.2.3 Miniaturized Projection Displays

Portable multi-media players have become a huge market during the last decade. Integration of wide screen miniaturized projectors to such devices to enhance the visual experience may very well become the next big thing in the gadget world. There are a handful of companies concentrated on the development such projectors using various technologies; such as the Digital Light Processor (DLP) [51], LCoS [52], and more promisingly scanning micromirrors. A WVGA (854 x 480 pixels) ultra compact projection display based on scanned 3-color laser light has been developed by Microvision, Inc. using a dual-axis MEMS scanning mirror [53]. The horizontal (fast) axis of the mirror operates at resonance (24 KHz) with optical scan angle of 60° . The vertical (slow) axis is driven off-resonance with a 60 Hz ramp signal. The laser light sources are red and blue laser diodes and a second harmonic green laser. Use of a MEMS and laser light sources leads to a small volume and a thickness of 7 mm, enabling the use of the projector system as a component embedded into portable consumer electronics. at a brightness of 10 Lumens, with a diagonal image size of about 1 m at a projection distance of 1.1 m. One additional advantage of this system is the lensless projection, enabling the projected image to remain in focus regardless of the device-screen distance. Microvision is not the only company working on microscanner based projector systems. Many major consumer electronics companies including Samsung [30], LG [32],

and Seiko [31] have developed similar systems with competent performance levels. Stand-alone or cell-phone integrated microscanner based projectors are expected to hit the market within 2009.

2.3 Remarks

The target wavelength range of the spectrometer developed within this thesis work is mainly the mid-to-far IR range, within which the detector cost and sensitivity are the major design considerations. Scanning Fourier transform spectrometers emerge as the only feasible choice for this wavelength range due to their single detector requirement and multiplex detection (all wavelengths are detected simultaneously) scheme.

The microlens array based laser scanning system developed in this thesis aims to combine the forward-looking scanning capability of the refractive approach with the speed and resolution of the reflective approach.

Chapter 3

ELECTROSTATIC COMB ACTUATION

An actuator is a transduction element converting a certain form of energy such as electric or thermal into mechanical motion by various means. The term refers to all such systems, without any restriction on size, speed, or motion range. Due to the size constraints, the common actuation methods used for MEMS devices predominantly have at least one dimension smaller than 1 mm; hence, the term microactuator is used to refer MEMS actuators. There is a broad range of microactuators used in conventional MEMS technology, including, but not limited to electrostatic, electromagnetic, piezoelectric, thermal, and shape memory actuators. The selection of a specific actuation scheme is completely application dependent, and is the first and maybe the most crucial decision to make in the MEMS design process. Performance of a microactuator is usually qualified through mechanical amounts such as force/moment or linear/rotary displacement. Usually, a microactuator can also be used as a position, force, or temperature sensor. For instance an electrostatic comb drive element can both function as an actuator when supplied with electric energy inducing motion but equally it can be used as a capacitive sensor in order to monitor displacements by measuring capacitance changes.

Explored in this chapter are the fundamentals of electrostatic comb drive actuation which is commonly utilized for the spectrometer and laser scanner platforms developed within this thesis. For the sake of brevity, and to remain within the context of next two chapters, the sensor aspects will be omitted.

3.1 Principles of Electrostatic (Capacitive) Actuators

Electrostatic actuation utilizes capacitive attraction forces (Coulomb's force) between charged objects to induce mechanical motion. In the macroscopic world, electrostatic actuation can rarely be used effectively due to large mass of the moving entities, and the limiting effect of electrostatic breakdown. Miniaturization mainly helps in two ways: the mass of the

moving entities scales down with the cube of the scaling factor, and the breakdown electric field increases significantly for very small gaps (a few microns) due to Paschen's law [56]. Hence, electrostatic actuation is significantly more effective for microscopic systems. When compared to other actuation methods, such as thermal or electromagnetic, electrostatic actuation has several advantages making it useful for MEMS systems. Sensitivity fast response time precision relatively easy fabrication or integration with CMOS technology are the most important of those advantages. Moreover, due to the design flexibility it presents, electrostatic actuation can be used to induce motion in all six degrees of freedom (along the three translational, and three rotational axes). Still, the main drawback of the electrostatic actuation is the relatively small amounts of actuation forces, especially compared to piezoelectric and thermal, and shape memory actuators.

There are the two basic configurations of a capacitor for electrostatic actuation of a MEMS device: the parallel plate and the interdigitated comb drive actuators. The motion of interdigitated comb capacitor is dominated by the fringe electrostatic field, whereas the parallel plate capacitor motion is dominated by the direct electrostatic field, and their static/dynamic characteristics are significantly different. The main focus of this section will be on the comb actuator side, since it is the main feature of the MEMS components that will be presented in the following two chapters. However, analysis of the parallel plate capacitor actuation is important to establish some important concepts.

3.1.1 Gap-Closing Capacitor

The simplest electrostatic actuator is in the form of two parallel plates, where one plate is held fixed, and the other one is free to translate towards the fixed plate, or rotate around its central axis, through flexible springs. Figures 3.1a, b are schematic views for a two-plate capacitor actuation in both configurations. Although seems simple, this type of actuation are widely used in the MEMS world. In the parallel plate configuration, neglecting the fringing fields, the capacitance for the actuator is simply

$$C = \epsilon \frac{w_x w_y}{g_0 - z} \quad (3.1)$$

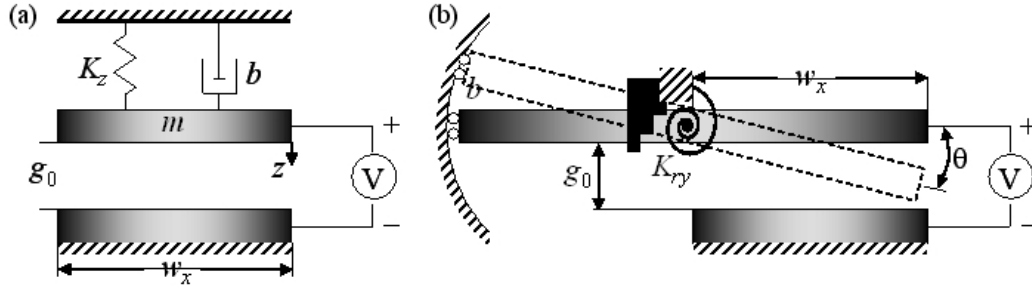


Figure 3.1: (a) An illustration of a parallel-plate electrostatic actuator with an applied voltage V and a spacing g_0 . The attractive force is normal to the plate surfaces. (b) Torsional electrostatic actuator.

The total electrostatic potential energy, W , of the system with a time dependent potential difference, $V(t)$, applied across the plate is found to be

$$W = \frac{1}{2} \epsilon \frac{w_x w_y}{g_0 - z} V^2(t) \quad (3.2)$$

The electrostatic force between two charged conductors is equal to the gradient of the stored potential energy. In this case, the only degree of freedom for the movable plate is along the z -axis; hence, the electrostatic force acting on the plates can be determined by differentiating the energy function, W with respect to the coordinate in the direction of the force:

$$F_e = \nabla W = \frac{\partial W}{\partial z} = \frac{1}{2} \epsilon \frac{w_x w_y}{(g_0 - z)^2} V^2(t) \quad (3.3)$$

Equation 3.3 reveals that electrostatic attraction force between the plates is inversely proportional to the square of the gap; hence, it is highly nonlinear. This nonlinearity is the reason behind the important phenomenon called pull-in: snapping of capacitor plates together due to a bifurcation that arises from the defined nonlinearity [61]. The following analysis of pull-in in the static and dynamic case is important to understand the dynamics of electrostatic actuation.

Static Pull-in

The quasi-static pull-in analysis of this system is well known and widely documented [57],[58], [59]. At the equilibrium point, the restoring force of the spring ($F_{mech} = K_z z$) and

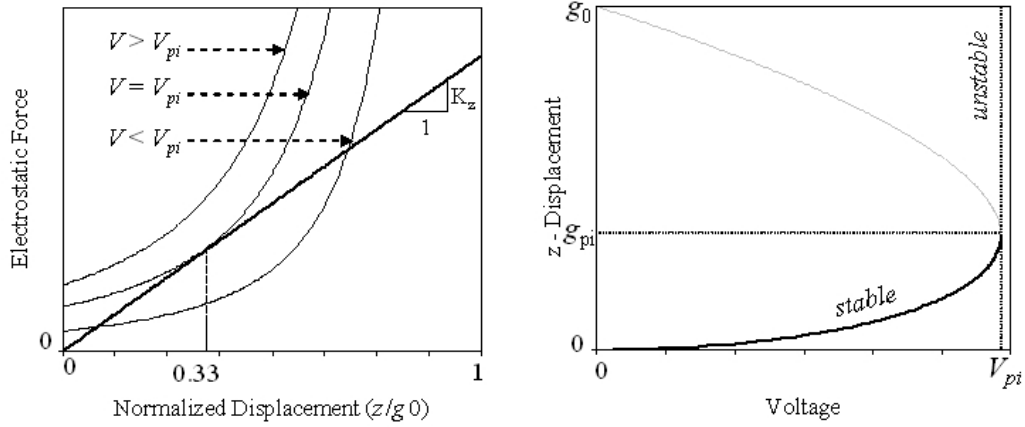


Figure 3.2: (a) Mechanical and electrostatic forces in a in-plane lateral mode mode comb actuator at three different voltage levels. (b) Voltage vs. deflection curve of a parallel plate capacitor.

the electrostatic force are equal:

$$K_z z = \frac{1}{2} \frac{w_x w_y}{(g_0 - z)^2} V^2 \quad (3.4)$$

Solving this equality for V yields the DC voltage required for a certain displacement:

$$V^2 = \frac{2K_z z (g_0 - z)^2}{\epsilon w_x w_y} \quad (3.5)$$

Figure 3.2 shows the nonlinear electrostatic force and the spring restoring force for increasing displacement. For relatively low voltages, electrostatic and restoring force curves have two equilibrium points. Only the lower of these equilibrium points is stable, since an increase in the gap due to a bifurcation can be corrected by the restoring spring force. At a critical voltage, which is called pull-in voltage (V_{PI}), two forces are tangent at a point; hence, there is single unstable equilibrium. Beyond the pull-in voltage, there is no stable equilibrium, and two plates snap. Pull-in occurs when the derivative of the voltage with respect to the displacement variable is zero. The deflection, z , at which the derivative of the voltage with respect to the position is zero is the deflection at pull-in, z_{pi} , and is found to be

$$z_{pi} = \frac{g_0}{3} \quad (3.6)$$

Note that the pull-in gap is a function of geometry only. The voltage at which pull-in occurs is computed by substituting 3.6 into 3.5.

$$V_{pi} = \sqrt{\frac{8}{27} \frac{K_z g_0^3}{\epsilon w_x w_y}} \quad (3.7)$$

For the torsional two-plate capacitor actuator, the DC voltage required to obtain a certain rotation angle θ is [62]

$$V = \sqrt{\frac{2K_{ry}\theta^3(g_0 - w_x\theta)}{\epsilon w_y [w_x\theta + (g_0 - w_x\theta)\ln(1 - w_x\theta/g_0)]}} \quad (3.8)$$

Please note that this particular expression is only valid if the bottom electrode size is equal to the size of the torsion plate. For a different electrode geometry, this expression will also be different; though the basic behavior of the actuator is similar. By numerically calculating the maximum of 3.8, the pull-in point is found to be:

$$\theta_{pi} = 0.44\theta_{max} \quad (3.9)$$

where θ_{max} is the maximum attainable torsional angle ($\theta_{max} \approx g_0/w_x$). The pull-in voltage associated with this point is given by

$$V_{pi} \approx \sqrt{0.83 \frac{K_{ry} g_0^3}{\epsilon w_y w_x^3}} \quad (3.10)$$

In many ways, the pull-in characteristics of the parallel and torsional two-plate actuators are very similar.

Dynamic Pull-in

Typically the analysis of the pull-in phenomenon is performed using quasi-static assumptions. However, it has been experimentally shown that under dynamic conditions, the pull-in voltage can be different from what the quasi-static analysis predicts [63]. A detailed analysis of dynamic pull-in under sinusoidal excitation conditions was presented by Nielson and Barbastathis [64]. Assuming that the motion of the moving plate is purely sinusoidal and in-phase with excitation, they proved that the pull-in gap, and pull-in voltage for a parallel two-plate actuator are given by:

$$z_{dpi} = \frac{g_0}{\sqrt{3}} \approx 0.58g_0. \quad (3.11)$$

$$V_{dpi} \approx \sqrt{\frac{2\pi K_z g_0^3}{3\sqrt{3}\epsilon w_x w_y Q}} \quad (3.12)$$

In the torsional case, pull-in angle and voltage becomes:

$$\theta_{dpi} \approx 0.73\theta_{max}. \quad (3.13)$$

$$V_{dpi} \approx \sqrt{3.21 \frac{\pi K_{ry} g_0^3}{\epsilon w_x^3 w_y Q}} \quad (3.14)$$

Dynamic pull-in analysis of Nielson and Barbastathis that yields the results above assumes that the excitation and the mechanical motion are in phase. However, for resonant systems, the phase relationship changes and gap can be much smaller before pull-in occurs [65]. The pull-in analysis shows that, the limited deflection before pull-in occurs is a significant design constraint that must be considered in the design process. Although it significantly limits the attainable deflection of two-plate capacitive actuators, there have been several MEMS devices that effectively utilize the pull-in phenomenon as a switching mechanism for a number of applications, including optical and radio frequency (RF) switching devices [63], [69], and light modulators [66], [68].

3.2 Electro-mechanics of Comb Actuators

In the previous section, it has been established that for two-plate capacitive actuation, there is a trade-off between the plate gap (hence, maximum available displacement), and actuation voltage minimization. An alternative type of electrostatic actuator with extended motion range, the so-called interdigitated comb actuator, was introduced by Tang et al [67]. This actuator is comprised of two sets of interdigitated electrodes (fingers), one of which is fixed, and the other is free to move. Similar to the two plate actuator, actuation is accomplished by applying a potential difference between the fixed and movable fingers. The geometry is such that the thickness of the fingers is small in comparison to their lengths and widths, and the gap between two fingers remains constant during motion; hence large travel ranges can be attained. For instance, Manzardo [11] has demonstrated more than 250 μm displacement with a comb actuated variable-depth grating device developed for lamellar grating interferometry. The more fingers used in the actuator, the greater the total capacitance, leading to a drastic decrease in the required voltage for displacements beyond a few microns. The use of comb-drives is important in decreasing the operation voltage

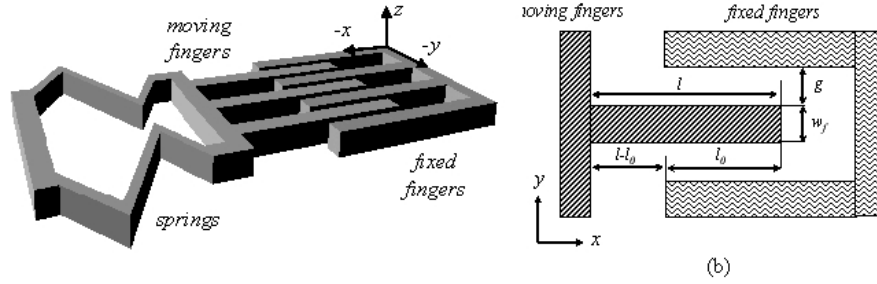


Figure 3.3: (a) Interdigitated comb finger actuator (b) Important dimensional parameters of a comb actuator

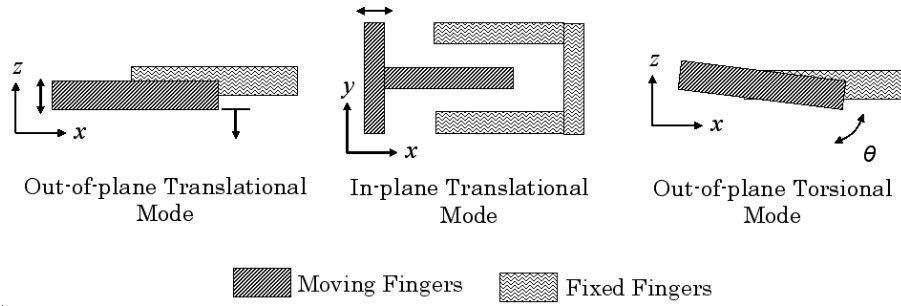


Figure 3.4: Moving finger motion of comb actuators for various actuation orientations

down to standard CMOS compatible range, which is an extremely important advantage in the sense of design flexibility and cost. Comb actuation was first used for actuation along the direction parallel to the substrate, for two primary reasons: they allow a larger displacement than parallel-plate actuation (tens of micrometers are feasible) and the force is relatively independent of displacement. However, several comb actuator variants have been proposed to induce motion along other directions that may or may not necessarily share those two qualities. Next, electro-mechanic characteristics of comb actuators classified based on motion orientation will be investigated.

3.2.1 In-plane Translational Actuators

In this configuration, the movable finger set is suspended by mechanical springs that only allow motion parallel to the fixed fingers, as shown in Figure 3.5a. Figure 3.5b plots how capacitance of a unit finger pair changes as a function of movable finger displacement. There are two capacitive effects that determine the shape of the capacitance-deflection curve of

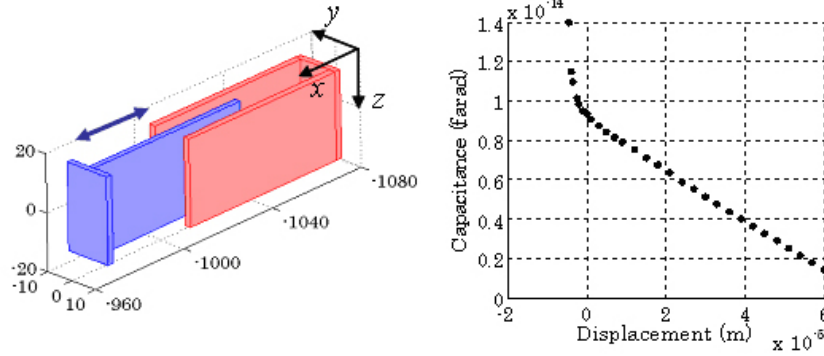


Figure 3.5: FEA (FEMLAB) simulated capacitance-displacement characteristics of a typical in-plane translational comb actuator. Simulation geometry: $t = 30 \mu\text{m}$, $w_f = g = 5 \mu\text{m}$, $l = 70 \mu\text{m}$, $l_0 = 69 \mu\text{m}$

this mode. When the fingers are almost completely engaged (left-hand-side of the plot), the capacitance between the tip of the finger and the frame is dominant, due to very low gap. As fingers disengage, this capacitance shrinks exponentially and becomes negligible when compared to the side-wall capacitances. Beyond that point, the decrease in the capacitance is linear with the displacement. This means that, except a very short range where fingers are almost completely engaged, the rate of change of capacitance is independent of displacement. Using the simple parallel-plate approximation (no fringing fields), at a time instant when finger displacement is x , the total capacitance is:

$$C_t = 2N \frac{\epsilon(l_0 + x)t}{g} \quad (3.15)$$

where, N is the total number of fingers. Hence, the force that generates the motion is calculated as:

$$F_t = N \frac{\epsilon t}{g} V^2(t) \quad (3.16)$$

It can be seen that the actuation force is constant, as contrasted to the case of a two-plate actuator where the force varied with the distance in a non-linear manner. The plus sign indicates that the electrostatic force favors the increase of distance y (or the increase of the overlap region between two adjacent plates). Despite the linearization of the force with respect to displacement, the in-plane comb actuator is still nonlinear with the excitation potential. Further linearization can be accomplished through the differential drive scheme, in which the mechanical structures are actuated with two sets of comb actuators that forces

the structure to move in the opposite directions. In such a configuration, if the excitation potentials of two comb sets are chosen such that

$$V_1(t) = V_{DC} + V_{AC} \sin(\omega t) \quad (3.17)$$

$$V_2(t) = V_{DC} - V_{AC} \sin(\omega t)$$

And, the resultant force becomes

$$F = F_1 - F_2 = N \frac{\epsilon t}{g} 4V_{AC} V_{DC} \sin(\omega t) \quad (3.18)$$

If V_{DC} , is kept constant, the net force of two actuators is linearly dependent on the time dependent component of the excitation potential. Hence, with this driving scheme, the associated device exhibit simple harmonic oscillatory behavior [70], and is potentially good for applications like mechanical resonators [71], tunable capacitors [72], and lens/grating platforms [73], where linearity is a desired property. The micro-optical scanner developed within this thesis also utilizes two sets of in-plane comb actuators with differential driving.

3.2.2 Out-of-plane (oop) Translational and Torsional Actuation

One striking feature of comb actuation is its capability of producing out-of-plane motion without the need of any non-planarity in the structure. Depending on the hinge mechanism of the movable structure, comb actuators can produce torsional or translational oop motion; the actuator characteristics are very similar for both cases, and therefore, they are treated together here. This type of actuation has been particularly popular for torsional micro scanning mirrors with relatively straightforward fabrication process. The MEMS lamellar grating structure that will be introduced in the next chapter also features a comb actuator in oop translation mode.

Electromechanical FEMLAB simulation results for translational and torsional oop mode comb actuators are given in Figures 3.6 and 3.7. Capacitance is highest at the rest position, and drops quickly as the tilt angle or the vertical displacement increases. This fast decrease in the capacitance slows down after the fingers disengage from each other. Beyond the disengagement, the capacitance is only due to the fringing fields. the capacitance change rate in this mode depends heavily on the angular displacement; therefore the force

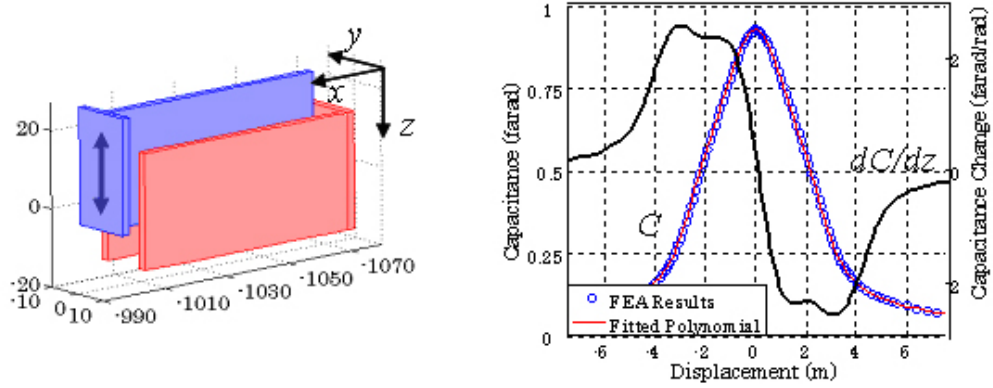


Figure 3.6: Capacitance vs. displacement and rate of change of capacitance vs. displacement characteristics of an out-of-plane translational mode comb-drive actuator.

is also displacement dependant. This dependence is the defining characteristics behind the *parametric system* behavior of oop comb-driven devices. This behaviour has been studied extensively by the author [74], [75], [76], [77]. An in depth analysis of parametric systems is also included in C.

Due to the symmetry of fingers in the vertical direction at the undeflected position, DC actuation is not possible [74] with plain comb actuators shown in Figure 3.3. However, several modifications have been made to the basic design by numerous research groups to obtain controllable DC motion, but mostly at the expense of increased fabrication complexity. Figure 3.8 shows four possible offset comb architectures and associated directions of motions together with the SEM picture of a device developed by Milanovic et. al. [78]. The device has four isolated vertical comb drive sets for up and down piston motion, as well as independent bidirectional rotation.

3.2.3 Rotational Comb Actuation

Electrostatic comb actuation has also been used to develop rotary motor structures. With these devices, a central rotor having surrounding capacitive plates is made to rotate by the application of voltages of the correct phase to induce rotation. Such devices have been shown to have a limited lifetime and require lubrication to prevent the rotor from seizing. The practical use has therefore been limited, but they are, nevertheless, the subject of

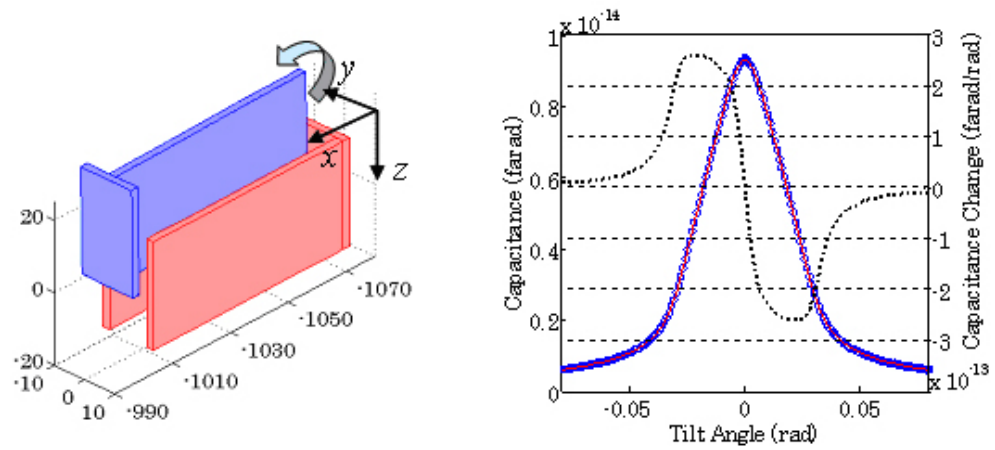


Figure 3.7: apacitance vs. displacement and rate of change of capacitance vs. displacement characteristics of an out-of-plane torsional mode comb-drive actuator.

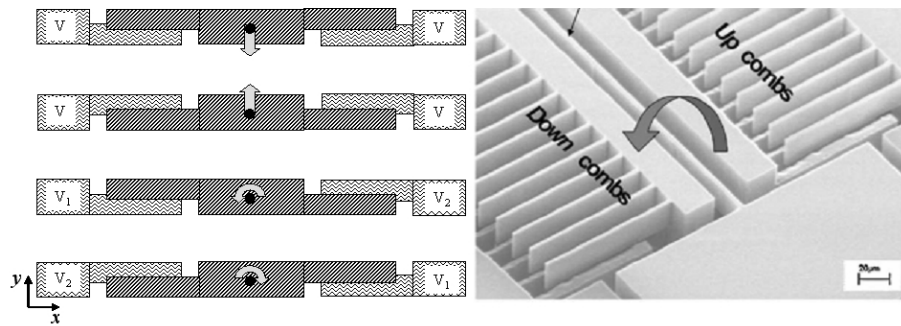


Figure 3.8: (a) Four possible offset comb drive set arrangements (b) SEM Picture of a device with four isolated vertical comb drive sets for up and down piston motion as well as independent bidirectional rotation [78].

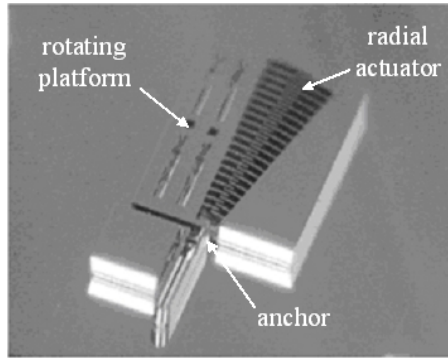


Figure 3.9: An Electrostatic Radial Comb-Drive Actuator [20]

intensive research [56].

When fixed-free digit pairs are placed concentrically, as it is in the rotational MEMS platform shown in Figure 3.9, the relative rotary motion can be generated or monitored in a manner similar to the one describing the linear longitudinal transduction. Application of an external electric field in a pair of fixed-mobile plates that can sustain relative rotary motion through adequate boundary conditions will generate tangential forces which will rotate the mobile part.

3.2.4 Lateral Pull-in in Comb Actuators

In principle, comb actuators do not suffer from the pull-in phenomenon, since the fundamental degree-of-freedom is not in gap closing direction. However, for some comb-driven devices with low directional selectivity springs, lateral pull-in (snapping of the fixed and movable fingers at each other) may also occur. Figure 3.10 shows the forces acting on a laterally moving comb finger. When fringing effects are neglected, the net electrostatic force on a single comb finger is given by

$$F = F_1 - F_2 = \epsilon l_0 t V^2 \left(\frac{1}{(g - y)^2} - \frac{1}{(g + y)^2} \right) \quad (3.19)$$

where ϵ is the permittivity of air, A is the total overlap area between the fingers, V is the DC potential between the complementary fingers, g is the initial gap between the fingers, and x is the lateral displacement. This lateral displacement produces a restoring force due to the springs, which is given by the expression

$$F_m = K_y y \quad (3.20)$$

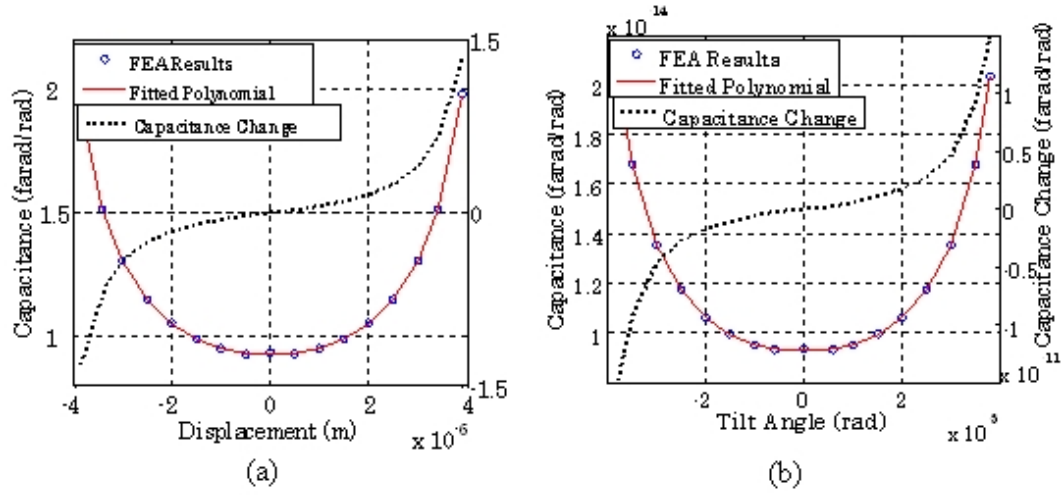


Figure 3.10: Capacitance-deflection and capacitance-changedeflection characteristics of a in-plane rotational mode comb-drive actuator.

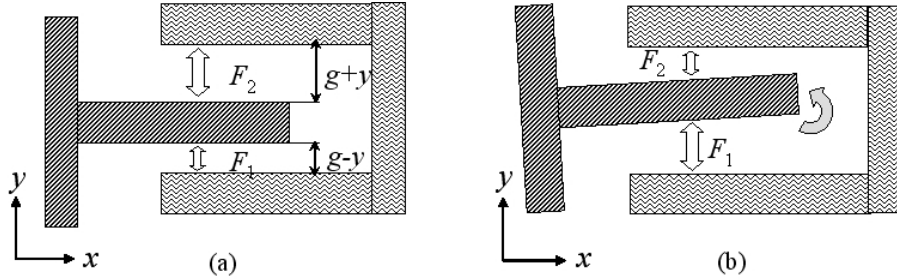


Figure 3.11: Electrostatic forces acting on a comb finger moving in in-plane lateral mode.

where K_y is the spring stiffness along the displacement. For relatively low voltages, electrostatic and mechanical forces have three equilibrium points, one of which is the rest position ($y=0$), as expected. When the system is at rest, a small change in the position (due to fabrication tolerances, shock or electrical noise) will disturb the equilibrium state. Since in the neighborhood of this equilibrium point mechanical force is stronger than electrostatic attraction, the system will again reach equilibrium. Therefore, the rest position is a stable equilibrium point. The situation is different when the system is at other two equilibrium states, since a small increase in the displacement will increase the electrostatic force more than the mechanical restoring force, and lead to pull-in. Thus, those states are unstable equilibriums. As the DC potential between the fingers is increased, unstable equilibriums

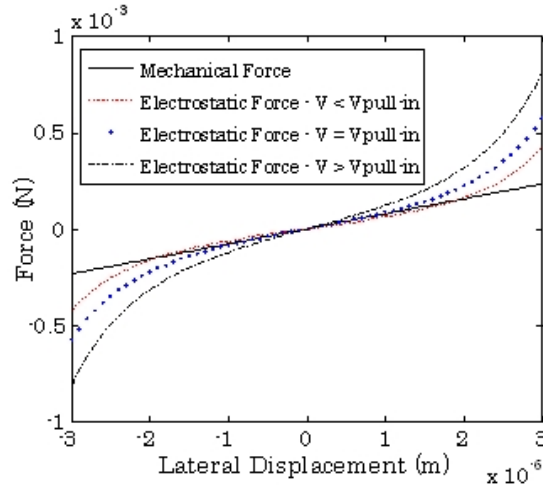


Figure 3.12: Mechanical and electrostatic forces in a in-plane lateral mode mode comb actuator at three different voltage levels.

come closer to the rest position. At a critical voltage, which is called pull-in voltage (V_{pi}), the rest position becomes the single equilibrium, which is unstable. At $V = V_{pi}$ slope of the electrostatic force at $y=0$ is equal to the slope of the mechanical force; in other words, two curves are tangent to each other. Beyond the pull in voltage, the system is always unstable. Figure 3.12 illustrates the graphical relation between the mechanical and electrostatic forces for three different voltages.

3.2.5 Remarks

The fundamentals of electrostatic comb drive actuation are investigated in this chapter. The theoretical framework established here will be helpful in the following two chapters, where the design and characterization of MES spectrometer and micro-optical scanner devices are discussed.

Chapter 4

COMB ACTUATORS FOR FOURIER SPECTROSCOPY

The fundamentals of electrostatic comb drive actuation were investigated in the previous chapter. In this chapter, a miniaturized Fourier transform spectrometer system designed for near-to-mid IR spectroscopy is presented. The system employs a MEMS variable depth lamellar grating device, in which the comb actuators are simultaneously utilized for actuation, phase modulation and optical position feedback. An overview of basic concepts of general and Fourier spectroscopy is included to provide a sufficient background on this important analytical technique. In the second subsection, design, fabrication and characterization of a MEMS lamellar grating spectrometer device are presented, followed by a detailed treatment of system integration issues and experimental spectral measurements. A discussion on the utilization of a novel polymer optical platform developed by Optical Microsystems Laboratory for FTS is also included in this chapter.

4.1 Elements of Spectroscopy

Spectroscopy is the study of the interaction between radiation and matter as a function of wavelength [79]. Originally, spectroscopy referred to the use of visible light dispersed according to its wavelength. Later the concept was expanded greatly to comprise any measurement of a quantity as function of either wavelength or frequency. Thus it also can refer to interactions with particle radiation or to a response to an alternating field or varying frequency. A further extension of the scope of the definition added energy as a variable, once the very close relationship between energy and frequency for photons was realized. A plot of the response as a function of wavelength - or more commonly frequency - is referred to as a spectrum. In this section, the fundamental elements of spectroscopy are briefly discussed.

4.1.1 Light and its Interactions

Light consists of oscillating quantized electric and magnetic fields. Because nuclei and electrons are charged particles, their motions in atoms and molecules generate oscillating electric fields. Depending on the relation between photon energy and oscillation of molecules, a number of different phenomena occur when a photon strikes a surface. A photon interacts with the surface mainly in three ways: it can reflect back from the surface, penetrate through it, or get absorbed. The nature of light-matter interactions can be understood through these three basic concepts. It may be argued that *scattering* is another form of light-matter interaction; however, it is actually a combined absorption-reflection process [80].

Reflection

Reflection is the change in the direction of a wave front at an interface between two different media so that the wave front returns into the medium from which it originated. Although the overall process can be interpreted as complete absorption and re-emission of a photon of exactly same wavelength and direction by the surface, the actual process is significantly more involved, since the polarization properties of light can be altered by the reflection process [81].

Refraction

Refraction can be defined as the bending of light rays that takes place when they pass through a region in which there is a gradient of the local velocity of propagation of the wave [82]. This is most commonly seen when a wave passes from one medium to another. Refraction of light is the most commonly seen example, but any type of wave can refract when it interacts with a medium, for example when sound waves pass from one medium into another or when water waves move into water of a different depth.

Absorption

An atom or molecule can absorb energy from light if the frequency of the light oscillation and the frequency of the electron or molecular “transition motion” match. If these frequencies match, the photon is destroyed and its energy is converted into an atomic or molecular process, such as an electron changing orbit, or a molecule changing vibration mode. The absorption wavelengths usually a characteristic of the absorbing material, forming the basis of spectroscopic analysis of materials

4.1.2 The Spectrum and the Spectrometer

A spectrum is a 2-dimensional plot for a compound signal that depicts the components by another measure, such as frequency or wavelength. A spectrometer is an optical instrument used to measure properties of light over a specific portion of the electromagnetic spectrum, typically used in spectroscopic analysis to identify materials. The variable measured is most often the light's intensity but could also, for instance, be the polarization state. The independent variable is usually the wavelength of the light, normally expressed as some fraction of a meter, but sometimes expressed as some unit directly proportional to the photon energy, such as wavenumber or electron volts, which has a reciprocal relationship to wavelength. A spectrometer is used in spectroscopy for producing spectral lines and measuring their wavelengths and intensities. If the region of interest is restricted to near the visible spectrum, the study is called spectrophotometry.

4.1.3 Infrared Spectroscopy

The infrared portion of the electromagnetic spectrum, shown in Figure 4.1 extends from 12500 cm^{-1} to 10^{-1} ; in wavelength scale, this range translates to the interval from $0.8\text{ }\mu\text{m}$ to 1 mm . The infrared spectrum is further divided into three regions: near-IR, mid-IR (fundamental IR), and far-IR. IR spectroscopy is the most commonly used spectroscopic method, because of its numerous advantages. The method is rapid, sensitive, easy to handle and provides many different sampling techniques for gases, liquids and solids. Important aspects are the convenient qualitative and quantitative evaluation of the spectra [83]. The features of an IR spectrum (number of infrared absorption bands, their intensities and their shapes) are directly related to the molecular structure of a compound, and they form a unique signature for it. The importance of the IR region stems from the fact that it comprises the fundamental vibrations of bound atoms. Hence, the absorption bands of such vibrations lies within the IR range of the spectrum.

4.2 Fundamentals of Fourier Spectroscopy

Basic operation principle universally shared by all Fourier transform spectrometers is summarized in Figure 4.2. The radiation from the source entering the interferometer is divided

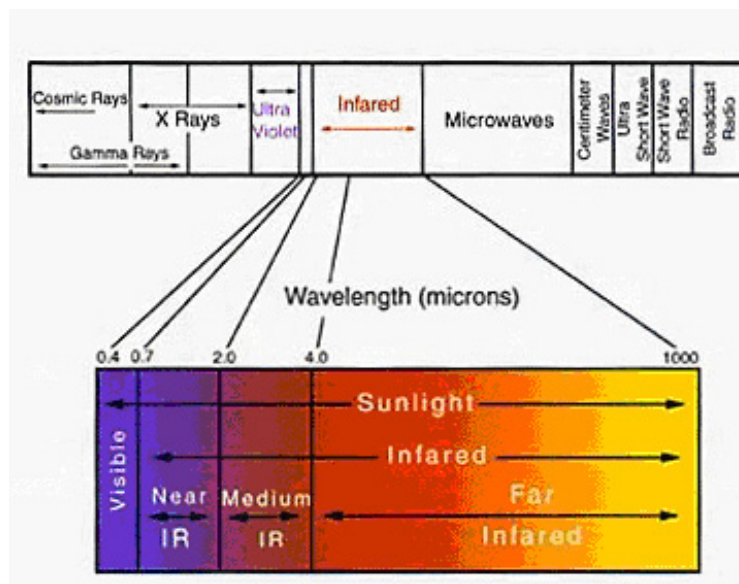


Figure 4.1: Infrared portion of the electromagnetic spectrum

into two mutually coherent beams, either by a beamsplitter (amplitude division) or diffraction grating (wavefront division). Two beams experience different optical paths before they are superimposed to interfere to yield an interferogram, which is defined as the intensity at a point on the interference pattern (recorded with a photodetector) as a function of the optical path difference. Lord Rayleigh found that the interferogram is actually a sum of cosine waves for all the wavelength components in the polychromatic source, multiplied, in each case, by a factor reflecting their intensity. Hence, the interferogram can mathematically be represented as the cosine transform, or the real part of a Fourier transform of the source spectrum. The invention of Fourier Transform Spectroscopy coincides with A. Michelson's invention of the Michelson interferometer in 1880. However, due to the lack of precision machining capabilities and Fourier transforming computers, FTS did not become a common measurement technique for the IR range, until 1970s. Traditionally, dispersive instruments, available since the 1940s, were used to obtain infrared spectra; however, in the recent decades, Fourier transform infrared (FTIR) spectrometers have succeeded over the dispersive instruments and became the predominantly used IR spectroscopy technique, vastly improving the spectral resolution and speed of spectral measurements. This subsection focuses on the fundamental elements of this enabling chemical inspection technique.

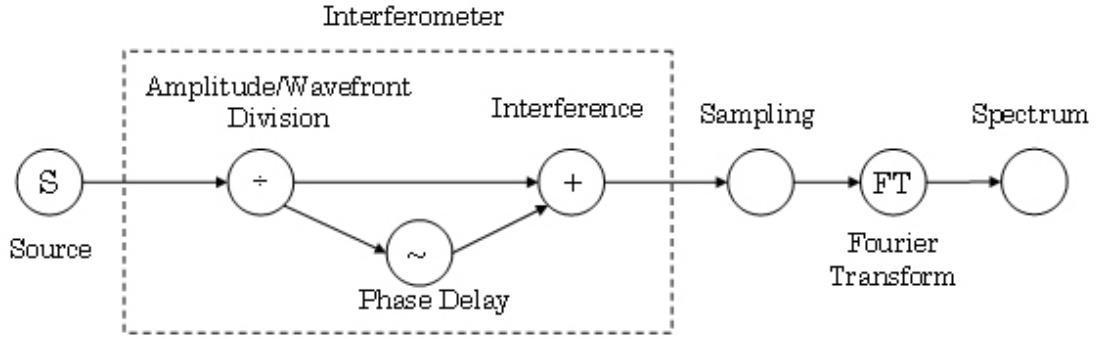


Figure 4.2: Basic operation principle of Fourier transform spectrometers. Such systems are classified based on the beam division method

Advantages and Limitations

FTIR has several advantages making it superior to the monochromators or spectrographs when used in the IR range. A qualitative list of these advantages is given below to illustrate the relevance of FTS for IR inspection.

The Jacquinot Advantage: The amount of IR light collected from the samples is generally weak in intensity, so it is crucial to collect and analyze this light very efficiently. Monochromators have an inherently low throughput. The diffraction grating produces a series of images only one of which is passed through the exit slit. In scanning grating instruments, the efficiency is further reduced because it samples only a small fraction of light at any given time. In FT spectroscopy, nearly half of the light entering the system is used at any given time; this improvement is known as the Jacquinot advantage [84]. Although less utilized, this advantage is also valid for the visible and ultraviolet region.

The Fellgett Advantage: For a given measurement, a Fourier transform spectrometer will detect far more photons and have a vastly superior signal-to-noise ration (SNR) than a scanning instrument, due to longer detector integration time. This is called the multiplex advantage. If the has analyzed spectrum has M spectral components, the SNR of a grating spectrometer and interferometric FTS has the ratio given by [85]:

$$\frac{SNR_I}{SNR_G} = \sqrt{M} \quad (4.1)$$

This advantage was first described by Felgett. All wavelengths entering the device are detected simultaneously on a single detector, and all data points on the interferogram contain

some information on them all. However, unlike spectrographs, the measurement time is not continuous, but should be an integer multiple of scanning period. The Fellgett advantage of FT spectrometers applies over spectrographs only if the performance of the instrument is limited by the SNR. Otherwise, for shot noise limited systems, it becomes a disadvantage.

The Connes Advantage: Theoretically, all spectrometers can show an improved SNR if spectra are averaged. However, this relies on the fact that the spectra can be exactly superimposed. Any displacement error between spectra will cause band shapes to be distorted and the SNR will fail to improve. Interferometers have the advantage that the frequency scale is generated from a laser reference signal, the wavelength of which is almost invariant. This enables spectra to be exactly superimposed, which is called the Connes advantage [86].

A less well-known but equally important advantage of FTS is the high attainable wavelength accuracy, due to the linear wavenumber scale derived from the highly stable laser reference interferogram specifying the sampling intervals [87]. The fact that the resolution is only limited by the scan path length, but not the grating size or detector pixel count should also be mentioned as one of the important advantages of FTS

There are two major limitations to FTS. The first is the cutoff at short wavelengths because of the need for a transmitting substrate to act as a beamsplitter, and the stringent optical mechanical tolerances required. However, as it will be clearly illustrated in this chapter, this limitation is valid only for Michelson interferometer based (amplitude division) FTS, but not the lamellar grating interferometry (wavefront division). The LGI system developed in this thesis do not require a beamsplitter and the alignment tolerances are inherently met by the precision micromachining process. Second major limitation of FTS is the requirement for a source with reasonably constant intensity over the scan period, since intensity fluctuations translate into spectral noise [87].

4.2.1 Interferogram and Spectrum

If two coherent beams with relative optical delay are superimposed, the intensity distribution in the region of overlap of the beams is found to have maxima which exceed the total intensity of the beams, and minima which can be as low as zero. This phenomenon is called *interference*. Position of the minima and maxima points (thus, the intensity profile of the interference pattern) is modulated by varying the optical delay, and this modulation

is recorded by a detector as a function of optical delay to yield the *interferogram*. The superposition of two strictly monochromatic source always gives rise to interference. However, light from any real source is never strictly monochromatic, and the amplitude and phase of the light undergoes extremely rapid fluctuations which are undetectable for any detector. If the two superimposed beams are originated from the same source, there is a certain level of correlation between the amplitude and phase fluctuations. Such beams said to be completely or partially *coherent*, and their superposition would produce observable interference. If the two beams are from different sources, the beams are mutually incoherent and no interference is observed under ordinary experimental conditions. Here, the interference between two *mutually coherent* monochromatic beams will be analyzed separately for amplitude and wavefront division interferometers to illustrate their equivalence. An intuitive extension of this analysis into broadband sources is also included in this section. The notions of interferogram sampling and resolution will be established on this analysis. A rigorous analysis of interferogram and spectrum relation in the context of coherence is presented in Appendix A for the interested reader.

Interference and Interferogram

The most common FTS method is the Michelson's spectral interferometer based on interference by division of amplitude. Figure 4.3 shows schematically the basic arrangement of the system. Light from a source is amplitude-divided at the beam splitter (O_1) into two beams, one reflected and one transmitted. Both beams travel to and reflect back from two mutually perpendicular and perfectly flat mirrors M_1 and M_2 , where former is stationary and the latter can move in parallel to the direction of beam propagation. Then, beams from M_1 and M_2 are partially transmitted and reflected onto the detector, respectively. Since the beam arriving to M_1 passes through the O_1 thrice, a phase plate of equal thickness and material is placed before M_2 in order to equalize the dispersive paths (this compensation element is not required if a cube beam splitter is used). The interferogram can be recorded with a photodetector placed at the center of the interferogram, as M_2 is scanned to modify the OPD between the interferometer arms. Let E_1 and E_2 be two coherent monochromatic sources given as

$$\hat{E}_1(\hat{r}, t) = \frac{1}{2} \left[\hat{A}(\hat{r})e^{-i\omega t} + \hat{A}^*(\hat{r})e^{i\omega t} \right]$$

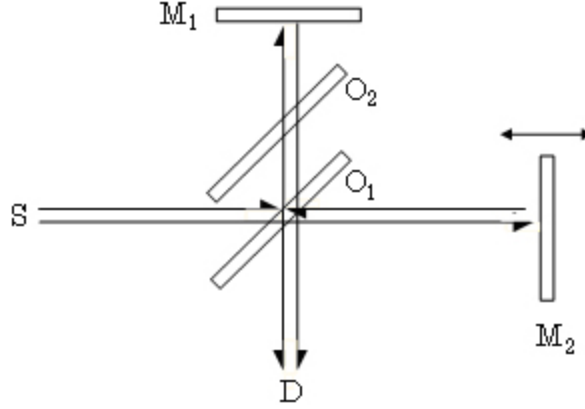


Figure 4.3: Fourier transform spectrometer in Michelson (amplitude division) configuration. O_2 is an optional phase plate to compensate for the possible dispersive paths between two arms of the spectrometer

$$\hat{E}_2(\hat{r}, t) = \frac{1}{2} \left[\hat{B}(\hat{r})e^{-i\omega t} + \hat{B}^*(\hat{r})e^{i\omega t} \right]$$

Here A and B are complex vectors with Cartesian coordinates

$$\begin{aligned} A_x &= a_1(\hat{r})e^{ig_1(\hat{r})}, \quad A_y = a_2(\hat{r})e^{ig_2(\hat{r})}, \quad A_z = a_3(\hat{r})e^{ig_3(\hat{r})} \\ B_x &= b_1(\hat{r})e^{ih_1(\hat{r})}, \quad B_y = b_2(\hat{r})e^{ih_2(\hat{r})}, \quad B_z = b_3(\hat{r})e^{ih_3(\hat{r})} \end{aligned} \quad (4.2)$$

Where, a_i , b_i and g_i , h_i ($i = 1, 2, 3$) are real functions. For a homogeneous plane wave the amplitudes a_i and b_i are real functions, whilst the phase functions g_i and h_i are of the form $g_i(r) = kr - \delta_i$, where k is the propagation vector and δ_i are phase constants which specify the state of polarization. The real phases g_i and h_i of the two waves will in general be different, since the waves will have traveled to P by different paths; but if the experimental conditions are such that the same phase difference σ is introduced between the corresponding components, we have

$$g_i - h_i = 2\pi\sigma\rho, \quad i = 1, 2, 3 \quad (4.3)$$

where ρ is the optical path difference (OPD) for the two beams from their common source to P , and $\sigma = \lambda^{-1}$ is the wavenumber. Suppose now that the two monochromatic waves E_1 and E_2 are superposed at some point P . The total electric field at P is

$$\hat{E} = \hat{E}_1 + \hat{E}_2 \quad (4.4)$$

So that,

$$\hat{E}_I^2 = \underbrace{\langle \hat{E}_1^2 \rangle}_{I_1} + \underbrace{\langle \hat{E}_2^2 \rangle}_{I_2} + 2\underbrace{\langle \hat{E}_1 \cdot \hat{E}_2 \rangle}_{J_{12}} \quad (4.5)$$

Where I_1 and I_2 are the intensities of the two waves and J_{12} is the interference term. In terms of complex amplitudes, the interference term is

$$\hat{E}_1 \cdot \hat{E}_2 = \frac{1}{4} \left(\hat{A} \cdot \hat{B} e^{-2i\omega t} + \hat{A}^* \cdot \hat{B}^* e^{2i\omega t} + \hat{A} \cdot \hat{B}^* + \hat{A}^* \cdot \hat{B} \right) \quad (4.6)$$

Such that,

$$J_{12} = \frac{1}{2} \left(\hat{A}^* \cdot \hat{B} + \hat{A} \cdot \hat{B}^* \right) \quad (4.7)$$

Substituting 4.3 into 4.6, followed by simple mathematical manipulation yields

$$J_{12} = (a_1 b_1 + a_2 b_2 + a_3 b_3) \cos(2\pi\sigma\rho) \quad (4.8)$$

This expression shows the dependence of the interference term on the amplitude components and on the phase difference of the two waves. Let us consider that A and B are waves propagations in the z -direction and are linearly polarized with their E vectors in the x -direction. Then,

$$a_2 = a_3 = b_2 = b_3 = 0$$

So that, the intensity terms in 4.4 becomes

$$\begin{aligned} I_1 &= a_1^2, \\ I_2 &= b_1^2, \\ J_{12} &= a_1 b_1 \cos(2\pi\sigma\rho). \end{aligned} \quad (4.9)$$

The total intensity as a function of OPD is then given by

$$I(\rho) = I_1 + I_2 + 2\sqrt{I_1 I_2} \cos(2\pi\sigma\rho) \quad (4.10)$$

In the special case $I_1 = I_2 = I/4$, which holds for an ideal Michelson interferometer, where I is the intensity of beam entering the interferometer, 4.9 reduces to

$$I(\rho) = \frac{I}{2} (1 + \cos(2\pi\sigma\rho)) \quad (4.11)$$

Hence, if $|\rho| = 0, 1/\sigma, 2/\sigma$, there will be maxima of the intensity, and when $|\rho| = 0, 1/2\sigma, 3/2\sigma$, there will be minima of the intensity. These conditions are called constructive and destructive interference, respectively.

Wavefront Division

In a Michelson interferometer, the beamsplitter inherently superimposes two arms at its output port. However, in a wavefront division interferometer, superposition of the split beams is accomplished through free space propagation, and the beams are said to be fully interfered beyond a certain propagation distance called *Fraunhofer distance*. Hence, the interferogram can be observed beyond this range. A rigorous analysis of the free space light propagation is presented in appendix B. Here, the results of that analysis will be used to derive the interference pattern for a rectangular binary grating, which is the common wavefront divider used in FTS. A beam of light can be split into two coherent segments after

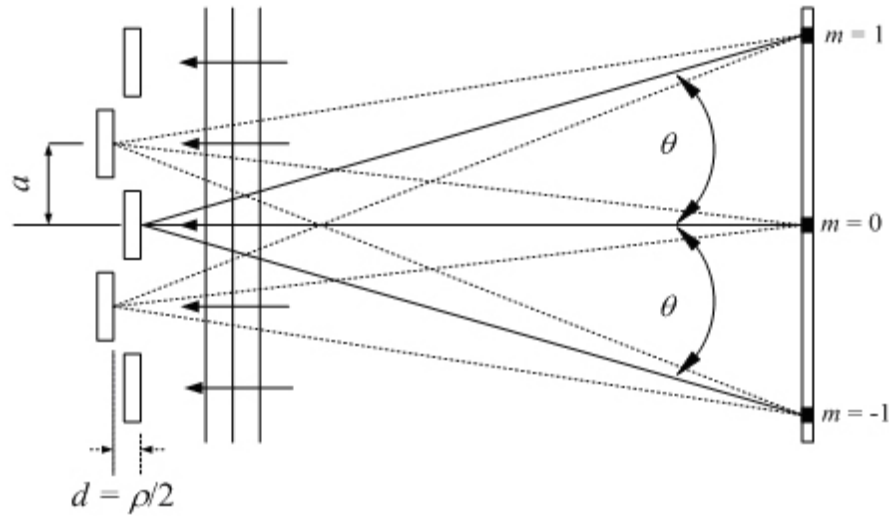


Figure 4.4: A binary rectangular diffraction grating. The optical path difference between the beam portions reflected from the back and the front facets is $2d$.

reflection from a binary diffraction grating. This system, which is based on a diffraction grating operating at the 0th order, is called a Lamellar Grating Interferometer (LGI), and has been discovered by Strong in 1960 [88]. A fully functional spectrometer system can be constructed using a grating with the proper period, and a single photodetector placed at $m = 0$. A novel FTS system based on a micromachined lamellar grating is developed in this thesis. An LGI has many advantages over a standard FTS, such as the lack of a beamsplitter, and speed. Moreover, simpler operation principle and fewer optical components enable construction of very compact spectrometers with good performance.

Assume again that a linearly polarized plane parallel radiation with the E vector in the x-direction is incident normally upon a binary diffraction grating as shown in Figure 4.4. Then the amplitude profile of the reflected light can be written as:

$$E_1(x) = \sqrt{I_1} \text{rect}\left(\frac{2x}{a}\right) * \left(\sum_{n=-N}^{n=N} \left(e^{i\frac{\phi}{2}} \delta(x - an + a/4) + e^{-i\frac{\phi}{2}} \delta(x - an - a/4) \right) \right) \quad (4.12)$$

where, N is the number of grating periods illuminated on one side of the symmetry axis and ϕ is the total phase difference between the wave portion reflected back from the front and back facets, which is given by

$$\phi = 2\pi\sigma\rho \quad (4.13)$$

Fraunhofer diffraction theory suggests that at the far field the amplitude profile of the propagating reflected light will take the form of the Fourier transform of the aperture function, which can be expressed as:

$$E_1(u) = \frac{\sqrt{I_1}a}{2\lambda z} \text{sinc}\left(\frac{au}{2}\right) \sum_{n=-N}^{n=N} e^{i2\pi anu} \left(e^{i\frac{\phi}{2} + i\frac{2\pi au}{4}} + e^{-i\frac{\phi}{2} - i\frac{2\pi au}{4}} \right) \quad (4.14)$$

Here, u denotes the spatial coordinate at the observation plane. The quadratic phase terms are ignored, since they do not affect the intensity profile. In order to be able to observe the diffraction profile at a close distance than the limits of the Fraunhofer region, the diffracted light can be focused with a lens. In that case, the amplitude diffraction profile on the focal plane becomes,

$$E(x) = \frac{\sqrt{I_1}a}{\lambda z_f} \text{sinc}\left(\frac{a\sigma x}{2z_f}\right) \cos\left(\frac{2\pi\sigma\rho}{2} + \frac{2\pi a\sigma x}{4z_f}\right) \sum_{n=-N}^{n=N} e^{i2\pi an\frac{\sigma x}{z_f}} \quad (4.15)$$

Hence, the intensity profile at the focal plane of the lens becomes,

$$I(x) = \frac{I_1 a^2}{\lambda^2 z_f^2} \text{sinc}^2\left(\frac{a\sigma x}{2z_f}\right) \cos^2\left(\frac{2\pi\sigma\rho}{2} + \frac{2\pi a\sigma x}{4z_f}\right) \frac{\sin^2\left(\frac{2\pi a N \sigma x}{z_f}\right)}{\sin^2\left(\frac{2\pi a \sigma x}{z_f}\right)} \quad (4.16)$$

The first multiplicative term in 4.16 is simply the square of Fourier transform of the rectangular aperture function replicated to form the grating. Its width is inversely proportional to the period of the grating. The third term is a train of *sinc* functions, resulting from the periodicity of the grating. As the grating period shrinks, the distance between the consecutive orders (diffraction angle) increases. Each order has a *sinc* shape, since the illuminated light is assumed to have a finite size. Spot-size of these orders shrinks as N increases. In

the ideal case, where illuminating light is of infinite span, this term becomes an impulse train. It is the second term that modulates the intensity profile based on the amount of phase difference introduced by the grating. Figure 4.5 plots (4.16) for three different values of grating depth ρ and three different N values. Please note that the total optical phase difference between the front and the back facets of the grating is twice the physical grating depth, due to reflection. At the center of the image plane, the diffraction intensity can be

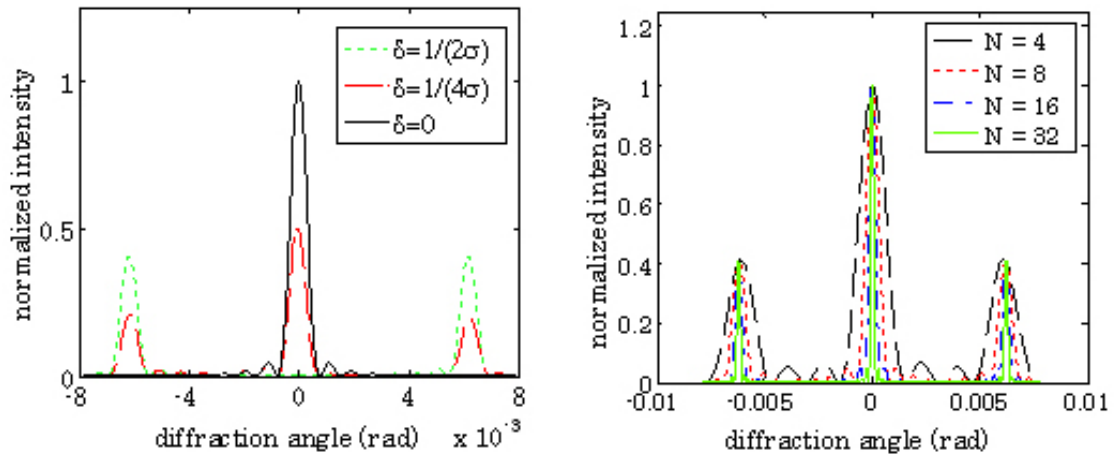


Figure 4.5: Intensity distribution at the angular diffraction plane evaluated from 4.16 for different phase difference values ($N=4$). (b) Width of diffraction orders with respect to the number of illuminated diffraction orders. Simulation parameters: $\lambda = 625$ nm, grating period $a=145$ μm .

computed as

$$I(\rho) = I \cos^2 \left(\frac{2\pi\sigma\rho}{2} \right) = I (1 + \cos(2\pi\sigma\rho)) \quad (4.17)$$

4.17 is identical to 4.11, proving the mathematical equivalence of amplitude and wavefront division interferometry. I accounts for the intensity coefficient in 4.16, which is equal to $\frac{I_1 a^2}{\lambda^2 z_f^2}$.

Interferogram vs. Spectrum

The signal $I(\rho)$ is the time-averaged intensity incident on a detector area at a specific OPD ρ . The interferogram is defined as the variable part of this detected signal:

$$F(\rho) = I \cos(2\pi\sigma\rho) \quad (4.18)$$

Assuming that a broadband source is constituted by continuous range of individual monochro-

matic components which are statistically independent from each other, this interferogram derivation can be extended from a monochromatic source to real light sources by summing the contributions from all spectral elements, assuming, since there will be no inter-component interference. Assume that a real source has a power spectral density designated by $B(\sigma)$ W/cm^{-1} ; then 4.17 becomes

$$\begin{aligned}
 I(\rho) &= \int_0^\infty B(\sigma) (1 + \cos(2\pi\sigma\rho)) d\sigma \\
 &= \int_0^\infty B(\sigma) d\sigma + \int_0^\infty B(\sigma) \cos(2\pi\sigma\rho) d\sigma \\
 &= \bar{I} + \underbrace{\int_0^\infty B(\sigma) \cos(2\pi\sigma\rho) d\sigma}_{F(\rho)}
 \end{aligned} \tag{4.19}$$

The first term in 4.20 is the constant intensity level in the absence of modulation. The interferogram is again defined as the modulated part. It can be deduced from 4.20 that the interferogram and the power spectral density form a cosine transform pair:

$$\begin{aligned}
 F(\rho) &= \int_{-\infty}^\infty B(\sigma) \cos(2\pi\sigma\rho) d\sigma \quad (W) \\
 B(\sigma) &= \int_{-\infty}^\infty F(\rho) \cos(2\pi\sigma\rho) d\rho \quad (W/\text{cm}^{-1})
 \end{aligned} \tag{4.20}$$

Equation 4.21 defines the fundamental relation for Fourier transform spectroscopy. Please note that the lower limit of integration is extended to $-\infty$. This extension has no effect since $B(\rho) = 0$ for negative ρ . Figure 2.6 illustrates two conceptual interferogram-spectrum transform pairs. Assuming, for the time being, that OPD space is of infinite extent, the interferogram produced by an ideal monochromatic source with wavenumber 0 will be a perfect sinusoidal signal, since the interfering beams are mutually coherent regardless of the relative optical delay. The cosine transform of this sinusoidal interferogram yields a single peak at 0, which is the power spectral density for an ideal monochromatic source. A typical interferogram of a realistic source is illustrated in Figure 4.6c. When OPD is zero, all wavelength components in the polychromatic light will interfere constructively to produce a high intensity peak at the center of the interferogram, which is called the *center burst*. At increasing OPD, the interferogram function decays, as would be expected of the sum of a large number of cosine waves, each with a different period.

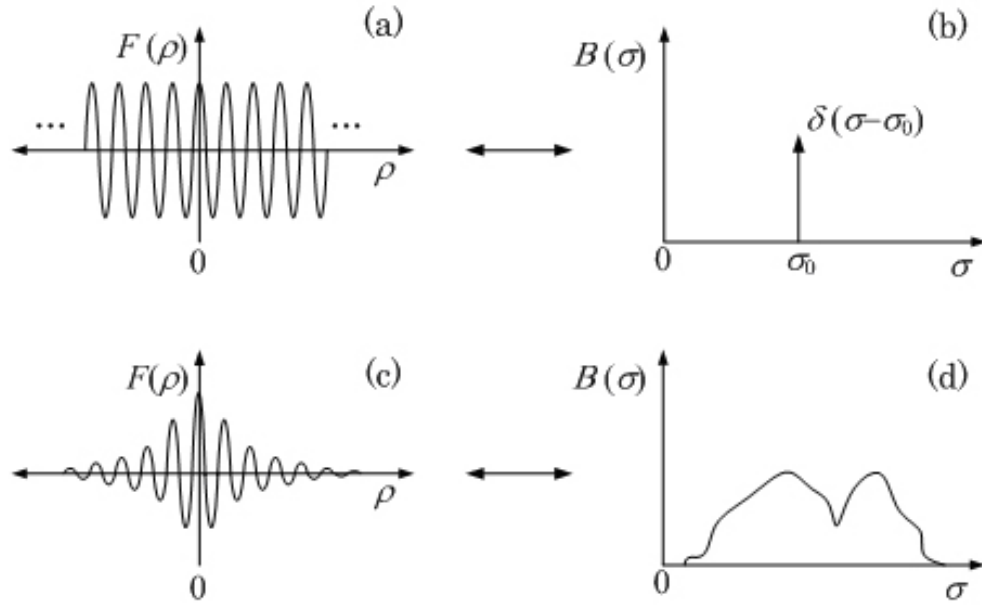


Figure 4.6: Two conceptual interferogram-spectrum pairs. (a) An infinite extent interferogram of an ideal monochromatic source of wavenumber σ_0 . (b) Associated spectrum (c) An infinite extent broadband interferogram and (d) associated spectrum.

4.2.2 Retrieval of the Spectrum

In the previous section, the mathematical relation between the interferogram and the spectrum was established. This section discusses the practical aspects and limitations of interferogram analysis.

Asymmetric Interferograms and the Complex Fourier Transform

So far, it has been assumed that the spectroscopy instrumentation was ideal, in which case, the resulting interferogram is a symmetric function of the path difference and only a single side is sufficient to retrieve the spectrum. However, for several reasons that are intrinsic to the interferometer, the measured interferogram may not necessarily be symmetric. In those particular cases, performing complex Fourier transform on the two-sided interferogram may be compulsory for accurate retrieval of the spectrum. There are two main reasons that an interferogram may not be symmetric. First, the center of the zero path fringe may not coincide exactly with one of the points at which the interferogram is sampled. Second, any dispersive effects make the position of zero optical path difference to be wavenumber dependent, so that this position may not in fact be well defined. Such

dispersive effects may be optical (small differences in the beam splitter and compensator thicknesses, for example) or electrical (frequency dependent delays in the amplifying or filtering electronics) [89]. With both of these factors taken into account, the interferogram expression in 4.21 can be rewritten as

$$F(\rho) = \int_0^\infty B(\sigma) \cos(2\pi\sigma(\rho - \epsilon) + \zeta(\sigma)) d\sigma \quad (4.21)$$

where, ϵ is the offset of the sampling grid, and $\zeta(\sigma)$ is the wavenumber dependant phase of the interferometer. The phase discrepancy due to the sampling offset is also wavenumber dependent and can be incorporated into the function ζ to yield

$$F(\rho) = \int_0^\infty B(\sigma) \cos(2\pi\sigma\rho + \phi(\sigma)) d\sigma \quad (4.22)$$

where, $\phi(\sigma) = \zeta(\sigma) - 2\pi\sigma\epsilon$. For a symmetric interferogram, $\phi(\sigma)$ is always zero. If complex Fourier transformation is applied to 4.22, one obtains

$$\begin{aligned} \mathcal{S}(\sigma) &= \int_{-\infty}^\infty F(\rho) e^{-2\pi i\sigma\rho} d\rho \\ &= e^{i\phi(\sigma)} B(\sigma) \end{aligned} \quad (4.23)$$

Hence, it is in fact possible to retrieve the spectrum by just taking the modulus of $\mathcal{S}(\rho)$; however this is undesirable, since it degrades the SNR by incorporating the noise from the imaginary part of the spectrum and also rectifies the noise [89]. A more common practice is to apply a phase correction to the complex spectrum to compensate for the phase discrepancy.

$$\begin{aligned} B(\sigma) &= \sqrt{\Re|\mathcal{S}(\sigma)|^2 + \Im|\mathcal{S}(\sigma)|^2} \\ &= \mathcal{S}(\sigma)e^{-i\phi(\sigma)} \end{aligned} \quad (4.24)$$

Hence, the real spectrum can be exactly reconstructed if the phase factor is known. There are several methods developed to extract the phase discrepancy information from the measured interferogram [90], [91], [92]. The reader is directed to these references for an extensive analysis of phase correction.

The phase error is, in general, a function of both frequency and path difference. There are three possible causes of path-difference dependent phase errors: tilt, shear, and sampling. Correction of path difference based phase discrepancy is more involved than 4.24 and

requires a careful modeling of the said sources. Each of those are reviewed, and its effect on the interferogram are modeled, elsewhere [93].

The ideal mathematical transform given in 4.21 can not be realized in practice because of the two main limitations. First, movable mirror or grating components can only move for a certain range, limiting the interferogram recording up to a finite OPD. The main consequence of this limitation is the degradation of the resolving power. The second major limitation is the discretization of the interferogram. The Fourier transform operation required to retrieve the spectrum is performed by digital computers; hence the interferograms have to be sampled before the processing.

Interferogram Sampling

Sampling can mathematically be represented as multiplication of the interferogram with an impulse train. Hence, the sampled interferogram can be written as

$$F_n[\rho] = F(\rho) \times \sum_{n=-\infty}^{n=\infty} \delta(\rho - n\Delta\rho) \quad (4.25)$$

The spatial distance between each impulse is $\Delta\rho$ and it defines the sampling period. Fourier transform of 4.25 yields the reconstructed spectrum:

$$\begin{aligned} B_r(\sigma) &= \mathcal{F}\{F_n[\rho]\} \\ &= B(\sigma) * \sum_{n=-\infty}^{n=\infty} \delta(\sigma - n\Delta\sigma) \\ &= \sum_{n=-\infty}^{n=\infty} B(\sigma - n\Delta\sigma) \end{aligned} \quad (4.26)$$

Therefore, the spectrum constructed from the sampled interferogram consists of a train of original spectrum replicas separated by a spectral distance $\Delta\sigma$. The sampling period and $\Delta\sigma$ are related by:

$$\Delta\sigma = 1/\Delta\rho$$

Spatial and wavenumber domain effects of the sampling operation are illustrated for a conceptual band-limited source in Figure 4.7. If the replicas are clearly separated from each other, the desired spectrum can be obtained by the following wavenumber domain filtering operation:

$$B(\sigma) = B_r(\sigma) \text{rect}\left(\frac{\sigma}{\sigma_{max}}\right) \quad (4.27)$$

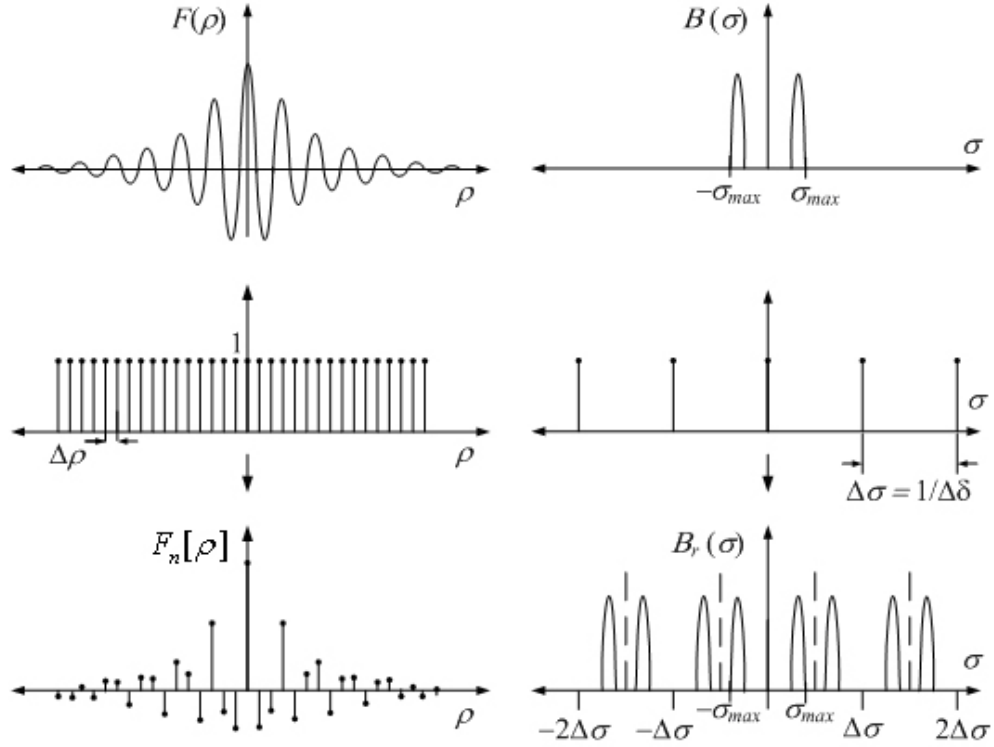


Figure 4.7: Effect of interferogram sampling on the spectrum. (a) Interferogram and spectrum of a band limited source. (b) Sampling function and its Fourier transform, which is periodic both in the spatial and spectral domain with periods $\Delta\rho$ and $\Delta\sigma$, respectively. (c) Sampled interferogram and its Fourier transform.

Hence, the desired band-limited spectrum can be perfectly reconstructed from the interferogram samples as illustrated in Figure 4.7, if the sampling period satisfies the condition

$$\Delta\sigma \leq \frac{1}{2\sigma_{max}} = \frac{\lambda_{min}}{2} \quad (4.28)$$

Equation 4.28 is called the Nyquist criteria and is an important concept in the field of signal processing. This result can also be intuitively deduced, since the smallest modulation period of such an interferogram will be λ_{min} , and one needs at least two samples to represent it. Sampling with a lower or higher frequency is called *undersampling* and *oversampling*, respectively. Given that the minimum spatial sample distance is $\lambda_{min}/2$, the minimum number of necessary samples is found to be:

$$N = \frac{\sigma_{max} - \sigma_{min}}{\Delta\sigma} = \frac{\rho_{max}}{\Delta\rho} \quad (4.29)$$

Aliasing If the Nyquist criterion given in 4.28 is not met, the tails of the replicated

spectra merge together and accurate reconstruction of the spectrum is no longer possible with wavenumber domain filtering. However, under certain conditions, the spectral band of interest can be reconstructed even from an undersampled interferogram. Generally, the spectral content of any interferogram is completely empty from 0 to σ_{min} , due to the long-wavelength cut-off of the detector, the source, or any optical filters used. In these circumstances it is possible to undersample without any ambiguity in the recovered spectrum. If the spectrum of the source is band-limited within the *free spectral range* given by $|\sigma_{max} - \sigma_{min}| < \Delta\sigma/2 = 1/2\rho$, the recovered spectrum is contained within one of the replications. Prior knowledge of the band limits is required to determine the correct replication to recover the spectrum. This can be ensured by controlled limitation of the spectrum with optical filters.

Sampling Errors Modern FTS systems employ processors that implement the infamous Fast Fourier Transform (FFT) algorithm to retrieve the spectrum from interferogram samples. This algorithm was developed by Cooley and Tukey [101], and is now the standard for digital Fourier transformation. The FFT algorithm takes for granted that there is a uniform spacing between the samples, which is given by the sampling period. For an interferogram, this means all consecutive samples should be separated by an equal OPD step. Any deviation from spatial uniformity of samples manifests itself as spectral noise. Random errors in the sampling time produce white noise, and periodic errors give rise to ghost with same general characteristics. Conventional FTIR systems employ a highly stabilized He-Ne laser beam following the same path through the interferometer as the signal beam. The zero crossings or the peak-valley points of the laser interferogram fringes are then used to trigger the sampling, so that the samples are equidistant from each other. For miniaturized FTS systems, incorporation of He-Ne lasers is impractical due to sheer size of these instruments. However, the MEMS technology enables the integration alternative precision position feedback mechanisms, such as capacitive [11], or piezoelectric, [94] detection. The interested reader is suggested to visit the incomplete list of publications in the references, for a comprehensive analysis of the spectral noise induced by the sampling errors [95, 96, 92, 97, 98, 99].

4.2.3 Instrumental Function: Resolution and Reproducibility

The instrumental function, also called apparatus function, instrument profile or instrument line-shape, is defined as the power spectral density which would be obtained for an ideal monochromatic source. The instrumental function includes any type of influence of the instrument over the spectrum, can be interpreted as the wavenumber domain impulse response of the spectrometer system. As for any other linear, time-invariant system, the spectral output of an FTS system will essentially be the convolution of the incident spectrum with the instrumental function. From this point of view, it is evident that the instrumental function determines resolving power of a FTS system. In this section, several factors that affect the instrumental function will be explored.

Truncation and Resolving Power

The interferogram formula given in 4.21 assumes that the OPD scan range is of infinite extent. However, for the real instruments, there is maximum attainable OPD, that we can define as ρ_{max} . Hence, the spectrum of an ideal monochromatic source with wavenumber σ_0 , whose spectrum consists of a single delta function as shown in Figure 4.6, retrieved from a finite-OPD interferogram would be

$$B(\sigma) = \int_{-\rho_{max}}^{\rho_{max}} \cos(2\pi\sigma_0\rho) \cos(2\pi\sigma\rho) d\rho \quad (4.30)$$

The integrand can be rewritten using a simple trigonometric identity:

$$\cos(2\pi\sigma_0\rho) \cos(2\pi\sigma\rho) = \frac{1}{2} [\cos(2\pi(\sigma_0 + \sigma)\rho) + \cos(2\pi(\sigma_0 - \sigma)\rho)], \quad (4.31)$$

The integrand is an even function of ρ ; therefore, we get

$$B(\sigma) = \left[\frac{\sin(2\pi(\sigma_0 + \sigma)\rho)}{2\pi(\sigma_0 + \sigma)} + \frac{\sin(2\pi(\sigma_0 - \sigma)\rho)}{2\pi(\sigma_0 - \sigma)} \right]_0^{\rho_{max}} \quad (4.32)$$

The first term is negligibly small for all positive values of σ ; so the effect of truncation can be represented by the second term only. Therefore,

$$B(\sigma) = \rho_{max} \frac{\sin(2\pi(\sigma_0 - \sigma)\rho_{max})}{2\pi(\sigma_0 - \sigma)\rho_{max}} \quad (4.33)$$

The instrument function is therefore the normalized *sinc* function given as

$$\rho(\sigma, \rho_{max}) = \rho_{max} \text{sinc}(2\sigma\rho_{max}) \quad (4.34)$$

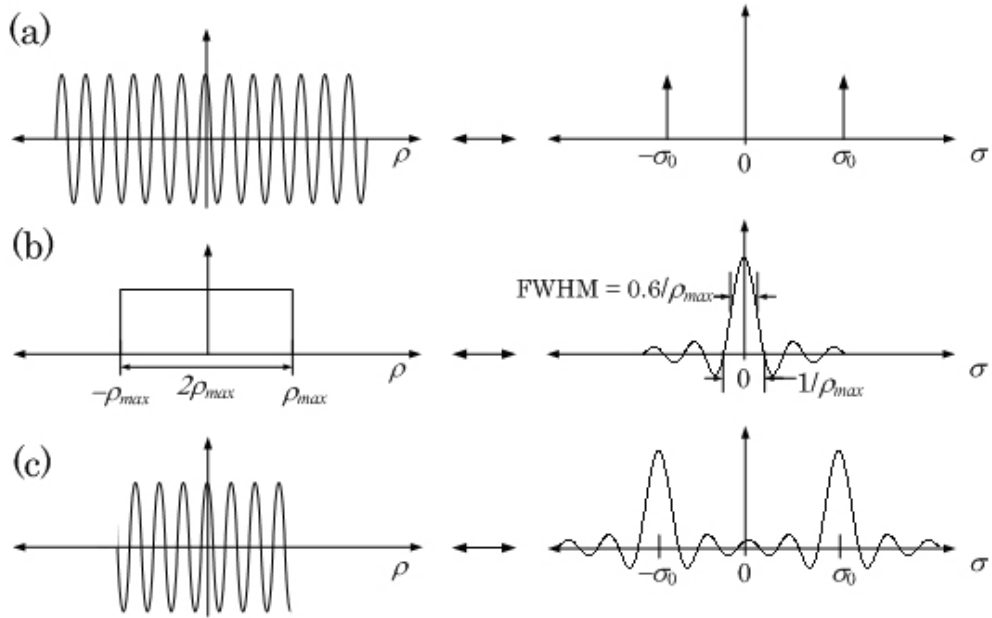


Figure 4.8: Fourier transform pairs for (a) The infinite extent monochromatic interferogram, (b) the boxcar truncation function, and (c) the truncated interferogram. Instrument line shape function of any practical spectrometer is a *sinc* function, whose width is inversely proportional to the maximum OPD.

The same result can be obtained using the convolution property of Fourier transform: the transform of the product of two transforms is equal to the convolution of their individual transforms. The truncated monochromatic interferogram can be represented by a product of an infinite extent cosine and a *boxcar* function, as given in Figure 4.8. Then, the spectrum recovered with complex Fourier transform will be the convolution of a sinc function with two impulses located at $-\sigma_0$ and σ_0 . The *instrument line shape* (ILS) function is defined as the Fourier transform of the boxcar truncation function, which is a *sinc*, as shown in Figure 4.8b. An important aspect of the *sinc* shaped ILS is that, a spectrum sampled perfectly has all the spectral points falling on the zero crossings of the ILS of every other point in the spectrum. There is no interaction and all spectral points are statistically independent. [100]. The *spectral resolution* of a spectrometer refers to its ability to distinguish closely separated sharp spectral lines, and is evidently related to the extreme path difference of the moving component. The formal definition of resolution differs from one spectroscopist to another, and it is often a good practice to explicitly state the convention used to define the resolution of a specific system. In this text, the *full width at half maximum* (FWHM) will

be used for the definition of resolution and resolving power.

$$\begin{aligned} \text{resolution} : \delta\sigma &= 0.6/\rho_{max} \\ \text{resolving power} : \mathcal{R} &= 1.66\bar{\rho}_{max}/\lambda \end{aligned} \quad (4.35)$$

It is evident from 4.35, that the spectral resolution of an FTS instrument is limited only by the maximum OPD between two beams. Practically, this may not always be the case, especially for high resolution bulk FTIR systems, since optical and alignment tolerances may become the limiting factors on resolution. However, for miniaturized spectrometers, in which the maximum path differences are moderate, this equation almost always holds. In the spectroscopy world, the resolution is given in the units of cm^{-1} . For a maximum OPD of 1 mm, the resolution is 10 cm^{-1} . State of the art bulk FTIR systems that are used for very high resolution applications can attain path differences up to 2m, which gives a resolution of 0.005 cm^{-1} .

Apodization

The side lobes of the *sinc* function reaches 20% of the central peak and may cause a ringing effect in the measured spectrum, if the acquired interferogram is not spatially confined within the truncation window of the instrument. This is a mathematical consequence of the abrupt truncation of the interferogram at the extremes of the path difference and it can be eliminated by multiplying the interferogram with a special function of choice to modify the truncation function. This actually gives the spectroscopist an opportunity to vary the instrumental function of the system according to the needs of the specific application. A common property of different functions of choice is that, their amplitude continuously becomes zero at the tails of the interferogram; hence the name *apodization*, literally *cutting off the feet*, is used for this tapering operation. A typical truncated monochromatic interferogram before and after apodization is shown in Figure 4.9. It is generally accepted that an apodization function optimum for all applications does not exist. Especially for high resolution astronomical applications, the optimum apodization function depends heavily on the nature of information to be extracted from the interferogram. Moreover, any apodization operation degrades the resolution, and destroys the orthogonality of the output samples; hence, one should be very careful in choosing the right apodization function in

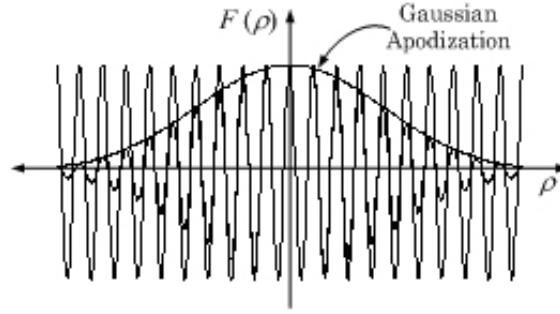


Figure 4.9: A truncated monochromatic interferogram with and without apodization. The apodization function is Gaussian, which suppresses the side lobes completely at the expense of roughly 50% loss of resolution

such demanding applications. However, for miniaturized spectrometers with relatively low resolving power, the difference due to the selection of apodization function is marginal. 4.1 lists the major features of five common apodization functions whose line shapes (spatial and wavenumber domain) are plotted in Figure 4.10.

Table 4.1: Some common apodization functions and their performance

	Apodizing Filter	Expression	FWHM	First Sidelobe Height
0	Unapodized	$rect(\rho_{max}, \rho)$	$0.603/\rho_{max}$	22%
1	Triangle	$1 - \rho /\rho_{max}$	$0.886/\rho_{max}$	4.7%
2	Connes	$(1 - \rho^2/\rho_{max}^2)^2$	$0.950/\rho_{max}$	1.3%
3	Cosine	$\cos(\pi\rho/2\rho_{max})$	$0.820/\rho_{max}$	2.9%
4	Hanning	$\cos^2(\pi\rho/2\rho_{max})$	$1/\rho_{max}$	0.84%
5	Gauss	$\exp(-\pi\rho^2/\rho_{max}^2)$	$0.963/\rho_{max}$	0%

Resolution Limit for LGI

For an ideal amplitude division FTS system, there is not a fundamental limit on resolution based on maximum OPD. Despite the practical limitations on system performance, such as mirror motion linearity, there are commercial FTS system that are capable of 2 m mirror displacement and retain a resolution at the theoretical limit [102]. For wavefront division

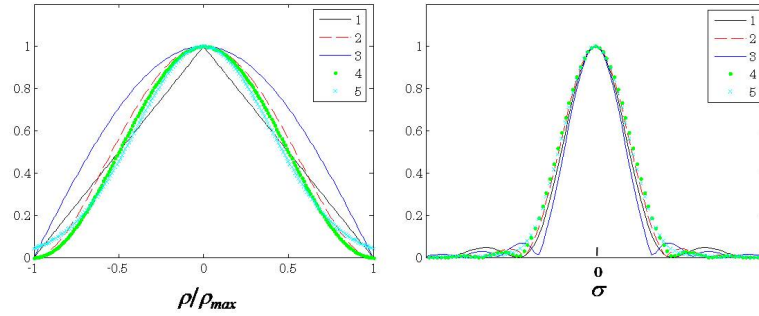


Figure 4.10: The shape of the five apodization functions given in 4.1 and their associated line-shape functions.

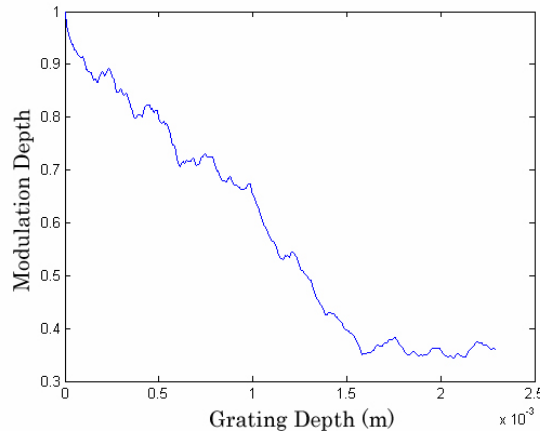


Figure 4.11: Modulation depth vs. Grating Depth. Simulation parameters: $a = 140\mu m$, $\lambda = 1\mu m$

FTS, however, the resolving power of the system can not be enhanced by increasing the maximum OPD beyond a certain limit. Hence, there is an absolute resolution limit. This is due to the diffraction of light along the path between the front and back facets of the grating, which destroys the identicalness of the interfering wavefronts. This effect can also be intuitively foreseen by considering that if OPD_{max} is greater than the Fraunhofer distance, the wavefront would diffract into several orders before reaching the back facets of the grating, which may not necessarily coincide with the position of the diffraction orders. Therefore, for visible-NIR-MIR LGI system, extreme path lengths that can be attained by Michelson FTIRs are not feasible. A good figure of merit for quantitative analysis of the

effect of large grating depths is the *modulation depth*, which can be defines as follows:

$$MD = \frac{I_{MAX} - I_{MIN}}{I_{MAX} + I_{MIN}} \quad (4.36)$$

where, I_{MAX} and I_{MIN} are the maximum and minimum intensity levels of a laser interferogram detected by a detector at the zeroth order. Figure 4.11 plots Fresnel diffraction simulation results for the decrease of the modulation depth with respect to the the grating depth. An analytical close form expression for the absolute resolution limit of an LGI spectrometer is difficult to achieve due to the hefty diffraction analysis required to predict the amount of light reflected back from the back facets. Hence, a numerical simulation approach was used to analyse this effect. The simulations are performed by numerical solution of Huygens-Fresnel diffraction integral for 140 μm period diffraction grating and a monochromatic source with 1 μm wavelength. As expected, around zero OPD, the modulation depth is at unity, which implies that the minimum level of the interferogram is zero. The modulation depth begins to decrease rapidly and reaches 60% of the initial value beyond 1 mm grating depth. The Fraunhofer distance for a grating with 70 μm features and 1 μm wavelength light is 5 mm, and rapid decrease of the modulation depth is observed at much shorter grating depths.

The main reason of the decrease in the modulation depth much before Fraunhofer distance can be understood by the analysis of the wavefront on its way towards the back facets. Figure 4.12 shows that profile for increasing grating depths. It can be deduced that due to the diffractive broadening, the total amount of light reflected back by the back facets decreases with increasing depth; hence, the light reflected back from the front and back facets of the grating becomes increasingly unequal, and the interferogram does not reach zero intensity level at destructive interference. The LGI system developed within this thesis targets the portable spectroscopy with moderate spectral resolution, therefore, the fundamental resolution limit shown here is not an important issue. Moreover, as long as the modulation depth is greater than the SNR of the system, the decrease in the modulation depth can be compensated by post-processing, but of course, at a cost of increased spectral noise.

4.2.4 Noise

In almost all spectroscopic measurements, there are variations in the signal that are not due to the absorption/emission process. Instead, these variations are caused by various factors

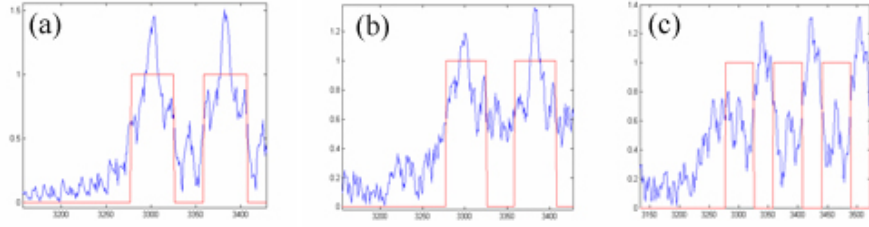


Figure 4.12: The modification of the wave amplitude profile after the front facets due to diffractive broadening

inherent in the process of the measurement itself, hence they are called noise. The main sources of noise and method to improve the noise performance of the system are discussed in this section.

SNR Signal-to-noise ratio is the dimensionless ratio of the signal power to the noise power that parameterizes the performance of an electrical or optical signal processing system. It can simply be defined as:

$$SNR = \frac{P_{signal}}{P_{noise}} = \left(\frac{A_{signal}}{A_{noise}} \right)^2 \quad (4.37)$$

more common practice is to define the SNR on logarithmic scale:

$$SNR = 10 \log_{10} \left(\frac{P_{signal}}{P_{noise}} \right) = 20 \log_{10} \left(\frac{A_{signal}}{A_{noise}} \right)^2 \quad (4.38)$$

The noise in an electro-optic system (detector, light source, etc) has three fundamental components, which are briefly introduced below. A detailed treatment of each noise component is out of the scope of this thesis, and can be found in the references [80], [103].

1/f Noise

$1/f$ noise (or pink noise) is a signal process with a frequency spectrum such that the power spectral density is proportional to the reciprocal of the frequency. The name arises from being intermediate between white noise $1/f^0$ and red noise $1/f^2$. Within the scientific literature the term $1/f$ noise is used loosely to refer to any noise with power spectral density of the form $1/f^\alpha$, where $0 < \alpha < 2$ with α usually close to 1 [104].

Shot Noise

Shot noise (or quantum noise, photon noise) is associated to the electromagnetic radiation itself and is a fundamental quantity. It refers to the fluctuations resulting from the random arrival of photons at the detector. The standard deviation of these fluctuations is proportional to the square root of the number of photons arriving. Therefore, the noise power is proportional to the square root of the signal power. Photon noise is a multiplicative noise. In the near-infrared and visible region, very sensitive detectors (e.g. photo-diodes) are available, which allow to approach photon-noise-limited detection. In this case, there is no multiplex advantage of the FTS over a dispersive instrument (note that the interferometer still enjoys the luminosity or etendue advantage).

Johnson Noise

Thermal noise (or Johnson noise, electronic noise, circuit noise) arises from thermal fluctuations of the current in the detector and is independent of the signal level. As mentioned above, in the infrared region, photon energies are smaller than these thermal fluctuations and, therefore, detectors will mainly be limited by the thermal noise. In addition, we mention the dark-current noise, which is an additive noise as well. Photodetection devices also generate dark-current noise, which is present even in the absence of light and results from random electron-hole pairs generated thermally or by tunneling.

Noise Minimization

The most effective way of reducing noise effects is signal averaging. Averaging over n scans improves the SNR by a factor of $n^{1/2}$. However, this improvement comes with the cost of increased measurement time, which might be an important issue for real-time time measurements, especially in low light conditions. Therefore, a miniaturized spectrometer should provide a rapid scan time to allow real-time operation in averaged measurements.

4.2.5 Sources and Detectors

The standard detector in routine FT-IR instruments is the pyroelectric DTGS (deuterated triglycine sulfate) detector, whose response in the MIR range is wavelength independent. The detector operates at ambient temperature and shows good linearity across the whole

transmittance scale. The main drawback of the DTGS detector is its response time, which is limited to a few KHz [105].

The MCT (mercury cadmium telluride) detector is much more sensitive and faster than the DTGS detector. A major disadvantage of the MCT detector is its spectral working range. Low wavenumber cut-off of a typical MCT detector is at around 600 cm^{-1} . Due to its low operating temperature, the detector element is covered by a vacuum enclosure with an optical window in front of the detector element. The vacuum housing increases the cost. Furthermore, the MCT detector shows nonlinear response, which can be minimized by special electronics and software. The MCT is the detector of choice for experiments in conditions of low radiation levels [105].

The broadband sources that are commonly used for FTIR spectroscopy are:

- NIR: Tungsten- halogen Lamp
- MIR: Globar (a 1.5 mm diameter and 20 mm long rare earth oxide electrically heated up to 2200 K), Nernst (a 5 mm diameter and 50 mm long silicon carbide rod electrically heated up to 1500K) or incandescent wire (a tightly wound coil of nichrome wire, electrically heated to 1100 K)
- FIR: High-pressure mercury lamp

4.3 Lamellar Grating Interferometer with Vertical Comb Actuators

Within the framework of this thesis, a novel FTS system based on out-of-plane vertical resonant comb actuators using simple fabrication on standard SOI wafers is presented. The comb fingers have dual functionality and used both for actuation and dynamic diffraction grating, which greatly reduces the die size and eliminates the use of a beam splitter and other IR optics. Since only a single reflector surface is used between the source and the detector, the system operation is broadband and suitable for visible, mid-wave and long-wave infrared bands at the same time. In addition, light collection surface of the design is the top surface of the wafer; therefore good surface quality and high light efficiency can be achieved. Due to the out-of plane actuation, the travel range of the fingers is not limited by the length of the comb fingers and the device operates without vacuum packaging.

The presented MEMS device is a novel platform for Fourier spectroscopy and subject to European patent EP1677086 with Dr. Hakan Urey and the author as inventors. The patent is now licensed by Fraunhofer IPMS, Germany. A collaborative Framework Programme 7 (FP7) project focusing on the development of a commercial microspectrometer device based on this patent, as well as an alternative approach, has been approved and granted by the European Commission.

This section explores the fundamentals of the developed spectroscopy method. Subsection 3.3.1 lists the fundamental specifications followed through the design procedure. Optical and mechanical analysis of the first generation spectrometer devices are presented in 3.3.2. Some major spectrometer system integration problems are addressed in 3.3.3, followed by a set of spectral measurements included in subsection 3.3.4. A summary of the second generation device fabrication procedure and a discussion on possible extensions of the project are given in the last subsection.

4.3.1 Specifications for a miniaturized MEMS LGI for mid-IR

Major issues that need to be addressed in spectrometer design are discussed in this section. Some of these specifications were set by the target performance of the device, and some arose due to the limitations of the fabrication process.

Spectral Resolution

The fundamental performance figure for a spectrometer is the spectral resolution. A potential LGI design should provide a reasonable resolution in order to distinguish itself from its counterparts. It was established in the previous section that (4.35), if the detector and Fourier transforming electronics are sufficiently fast, the spectral resolution for a LGI is proportional to the wavenumber of the measured light, and the maximum displacement of the moving fingers. In order to improve the spectral resolution, the travel range of the device is maximized, and meeting this requirement is the prime mechanical design objective. The target resolution for the grating structure designed in this work is 20 cm^{-1} , corresponding to $\pm 100 \text{ }\mu\text{m}$ deflection.

Isolation of Diffraction Orders

For spectroscopy application, one is only interested in the intensity of the 0th diffraction order; therefore the light going to this order should be well-separated from the light going to other orders. To achieve good separation between orders, small focused spot size on the detector plane and large angle between diffracted orders is desired, which requires wide clear aperture, and small grating period, respectively (4.16).

Operation Frequency

In order to loosen the speed requirements on the detector at the diffraction plane, the oscillation frequency of the device should be kept relatively low (less than 1KHz), but not too low as the spectrum can change during data collection. Therefore a specific data rate can be reached with a slower/cheaper detector; or faster spectral measurement can be maintained with the same detector. Speed and amount of data flow through the detector is crucial, since both of these parameters may degrade system performance, if requirements are not satisfied. A good target specification is 1 msec measurement time, that would allow subsecond measurement for up to 1000 times averaging.

Operation Voltage

Operation Voltage of the device should be kept below 70 V, in order to meet the design rules of Fraunhofer IPMS. Similar to the IPMS microscanners, electrical isolation of the comb-finger pairs will be accomplished by isolation trenches, which can withstand a maximum potential of 70 V.

Shock Resistance:

During the microfabrication and packaging process, the device may experiences mechanical shocks as strong as 2000 times the gravitational acceleration. If the device breaks downs during fabrication, as a result of such a shock, it may damage the process. The flexures of the device should be designed to withstand such large acceleration forces without breakage

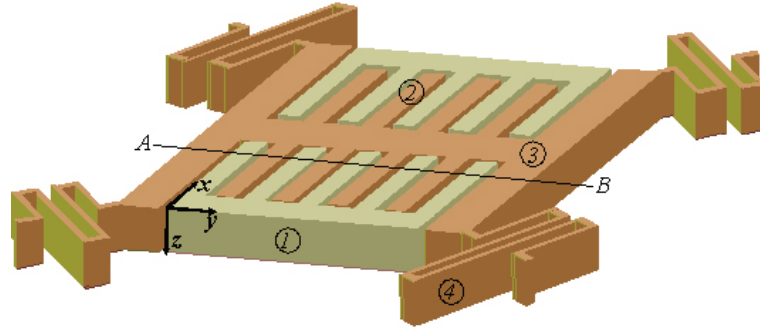


Figure 4.13: Spectrometer device structure. (1) Fixed Fingers, (2) Movable Fingers, (3) Rigid Backbone, (4) Folded Flexures. (The figure is not drawn according to actual proportions.)

4.3.2 Lamellar Gratings Interferometer with Vertical Comb Actuators

Comb actuators, as explored in Chapter 3, are tiny periodic structures inherently in the form of a diffraction grating. Therefore, a set of comb actuators translating in the out-of-plane direction with reflective top surface is essentially a variable-depth diffraction grating that can be utilized as a LGI. This concept constitutes the basic principle behind the developed spectroscopy method. Design fabrication, and testing of such an out-of-plane comb actuated MEMS grating device has been a main focus of this thesis work. Development of smart methods to integrate this MEMS device into a spectrometer system was also a major field of interest. Here, a comprehensive presentation of the work done for those tasks is given.

Mechanical Properties of the MEMS Grating

An illustration of the grating structure designed for the spectrometer application is shown in Figure 4.13, and the actual dimensional parameters are defined and listed in Figure 4.14, and Table 4.2, respectively. At the core of the structure, there are two sets of electrostatic comb actuators that are simultaneously utilized as an actuator and a variable-depth diffraction grating. The comb sets are symmetrically placed on a rigid backbone, which enhances robustness of the structure and helps to keep the device surface flat while resonating. Width and length of these comb fingers (both static and movable) are $70\ \mu\text{m}$ and $1.2\ \text{mm}$, respectively, where the gap between them is $5\ \mu\text{m}$ (please see 4.2 for detailed information on device dimensions). High width-to-gap ratio of the comb fingers ensures a high fill factor

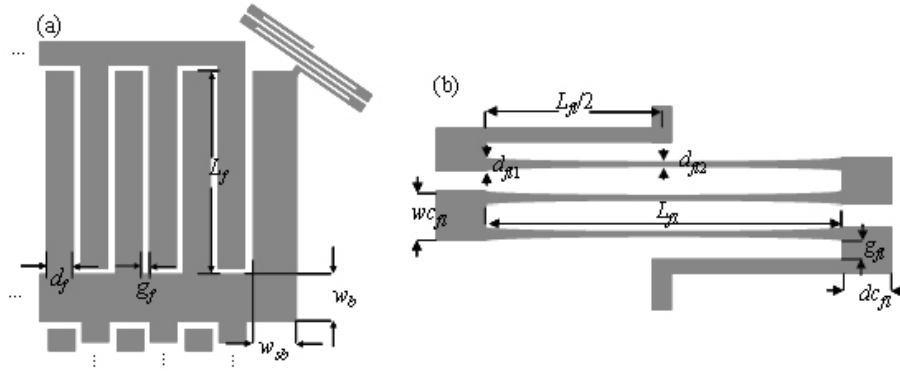


Figure 4.14: (a) Spectrometer dimensions. The figure shows the upper-right corner of the device, together with the fixed flexures. (b) Flexure dimensional parameters.

and therefore, good optical efficiency; however it also increase the grating period, which is unfavorable due to smaller separation between diffraction orders. Comb fingers are coated with a 100nm thick layer of aluminum, hence, reflectivity of the fingers are enhanced for the visible and infrared region, allowing broadband operation. The 250 μm wide H-shaped backbone that carries the movable fingers is connected to the fixed frame via four folded flexure beams. Figure 4.14b is a close-up view of one of the folded flexures which have varying cross-sectional width to have uniform stress distribution and to provide low stiffness within a compact structure. Low stiffness springs lower the resonant frequency and speed requirements of the readout electronics, and increase the travel range, which improves the spectral resolution.

The dimensions of the device were optimized based on the optical and mechanical specifications, as well as the fabrication limitations. In terms of optical performance, the most important parameters are of those of the comb fingers. The period of the fingers determines the optical efficiency and the diffraction order separation, which are antagonistic properties. Due to fabrication limitations, the gap between the static and movable fingers was set to its minimum value of 5 μm . With a fixed gap size, shrinking the grating period also decreases the fill-factor, which leads to a reduction in optical efficiency. However, smaller grating pitch (period) is desirable for stronger diffraction effect, which leads to better separation of diffraction orders, and hence, allowing smaller overall system dimensions. During the design process, an optimum point simultaneously satisfying those two criteria has been sought.

Table 4.2: Dimensions of the MEMS spectrometer device. Device thickness is 30 μm

Dimension	Symbol	Value (μm)
Device length	D_1	2650
Device width	D_2	2700
Central beam width	w_b	250
Side beam width	w_sb	250
Comb finger length	L_f	1200
Comb finger width	d_f	70
Gap between fingers	g_f	5
Flexure fold length	L_{fl}	1200
Fold corner width	wc_{fl}	30
Fold corner length	dc_{fl}	40
Flexure max width	dc_{fl1}	10
Flexure min width	dc_{fl2}	5
Gap between flexure folds	g_{fl}	20

With the dimensions given in Table 4.2, the optical efficiency is 80% (determined using custom written Fourier Optics analysis software) and the diffraction angle is 7 mRad (please see Figure 4.24). This diffraction angle was enough to get a 0.7 mm separation between diffraction orders after 10 cm from the grating surface; this much separation enables the isolated recording of the central order intensity with a detector of 0.5 x 0.5 mm² active area. The length of the fingers, on the other hand, kept as long as possible, given the limitation on the maximum die area, which was provided by Fraunhofer IPMS - the foundry who did the fabrication.

The dimensions and shape of the suspension beams are crucial for device performance. The major objective in designing the flexure beams were to keep the vertical translation of the device as the fundamental operation mode, while at the same time pushing the 2nd and higher natural modes sufficiently away from the first mode. The sufficiency of mode separation was defined intuitively, such that the 2nd natural mode frequency lies above 1.5 times the fundamental mode frequencs. Various suspension geometries have been

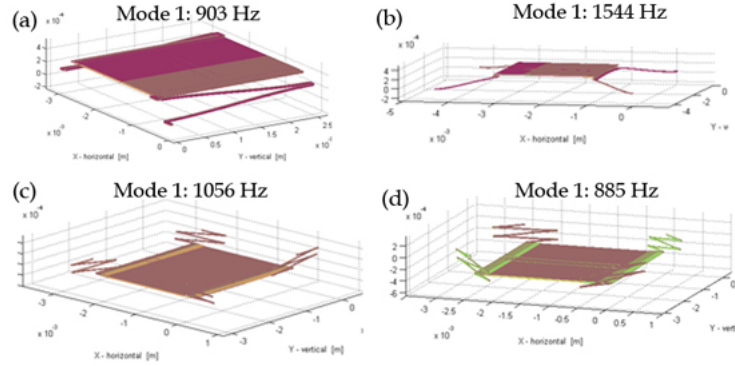


Figure 4.15: Out-of-plane translation mode shapes for mirrors with various suspension geometries, and their respective mode frequencies. The 45 degree folded suspension structure shown in (d) has the lowest mode frequency, and best mode separation (Second natural mode frequency is at 1560Hz, which is sufficiently away from the fundamental mode).

investigated to obtain a low stiffness from a compact structure, some of which are shown in Figure 4.15 (please note that the mode frequencies were simulated for a rigid mirror, rather than a grating, for the sake of simplicity). The 45° folded flexure configuration with variable cross-section has demonstrated the best performance in terms of mode separation, stress distribution, and stiffness reduction. Using varying width flexures fold arms instead of uniform beams helped in two ways. First, it lowered the fundamental mode frequency while not degrading the mode separation performance, and second, it lowered the maximum stress for a some level of shock, despite the decrease in stiffness. This was accomplished due to a much more uniform stress distribution along the along the flexure. FEA comparisons have showed that non-uniform cross section beams provided 20% improvement on both aspects.

Modal Characteristics

For a MEMS structure design to be operated in the resonant mode, distribution of the natural mechanical oscillation modes (*eigenmodes*) on the frequency domain is of utmost importance. If these eigenmodes are not sufficiently away from each other, energy coupling between them may occur. The result of this coupling might vary from degradation of device performance to complete device failure, such as electrostatic breakdown due to lateral pull-in. To investigate the eigenmode distribution of the spectrometer device, COVENTOR (finite element analysis software package) simulations were performed. Figure 4.16 shows

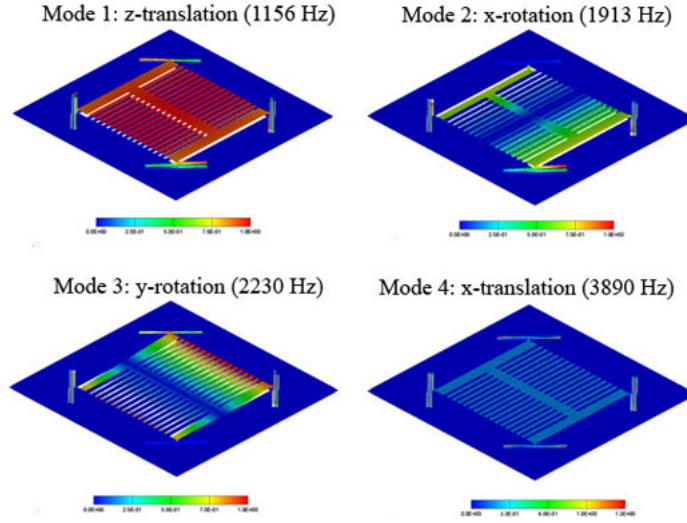


Figure 4.16: First four natural oscillation modes (eigenmodes) of the movable grating device. The fundamental mode corresponds to the out-of-plane translation of the movable fingers, and it rests sufficiently away from the second oscillation mode. Moreover, the fundamental mode frequency is 15% larger than the target frequency of 1 KHz.

the first four mechanical modes of the device. Modal analysis results show that the fundamental eigenmode of the structure is the out-of-plane translation mode, and the separation of the first and second eigenmodes are sufficiently good. This ensures that there would be no mechanical motion coupling between these modes to force the device to deviate from pure z-translational motion. For the spectroscopy application, this is a crucial requirement, since even a motion nonuniformity in the order of $\lambda/4$ is sufficient to disturb the interferogram. The fundamental reason behind the good modal characteristics is the spring system consisting of four folded flexures.

4.3.3 Fabrication

The designed device was fabricated on a 30 μm thick silicon-on-insulator (SOI) wafer with a CMOS compatible process by Fraunhofer, IPMS in Dresden, Germany. The available process was standardized by the foundry; hence the device had to be designed to be compatible with the process. Furthermore, the fabrication was actually a multi-project wafer run; hence, the available die area reserved for this device on the wafer was very limited. Both these effects significantly limited the design space, and only two variant of the sme

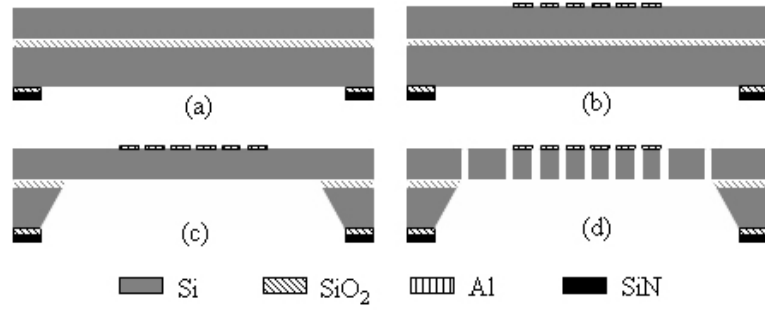


Figure 4.17: Schematic process flow for the movable grating. The drawings correspond to a cross-section along the line A-B defined in Figure 4.13

device could be fabricated (one is a device with uniform cross-section flexures, and one is the one shown here). However, IPMS had a standardized and well-controlled process leading to a high yield (%90), and good device uniformity.

Fundamental steps of the fabrication process developed by Fraunhofer, IPMS for a torsional microscanner technology [112] are shown in Figure 4.17. The $30\text{ }\mu\text{m}$ thick structural layer is highly doped to maintain conductivity. For the definition of wire-bond electrodes, an oxide and a metal layer is deposited on the structural layer and then patterned (not shown in the figure). On the backside of the wafer a hard mask consisting of an oxide and a silicon nitride layer is deposited and patterned for a further backside etching step (Figure 4.17a). Then, a 100 nm thick Aluminum layer is deposited on the movable and fixed fingers to enhance reflectivity (Figure 4.17b). The substrate under the mechanical elements and the buried oxide are subsequently etched away in a TMAH and an HF solution (Figure 4.17c). Finally, the mechanical elements are released by an ASE deep silicon etching process. Microscope pictures for one of the fabricated devices are shown in Figure 4.18a, b, and c.

4.3.4 Actuation and Modeling

In resonance, operation mode of the comb-actuated device is determined by the excitation frequency and the eigenmodes of the mechanical structure. By varying the excitation frequency, a comb-actuated structure can be oscillated in different mechanical eigenmodes. The modal analysis results given above suggests that at its fundamental oscillation mode, the spectrometer can be modeled as a single degree of freedom system, which means that

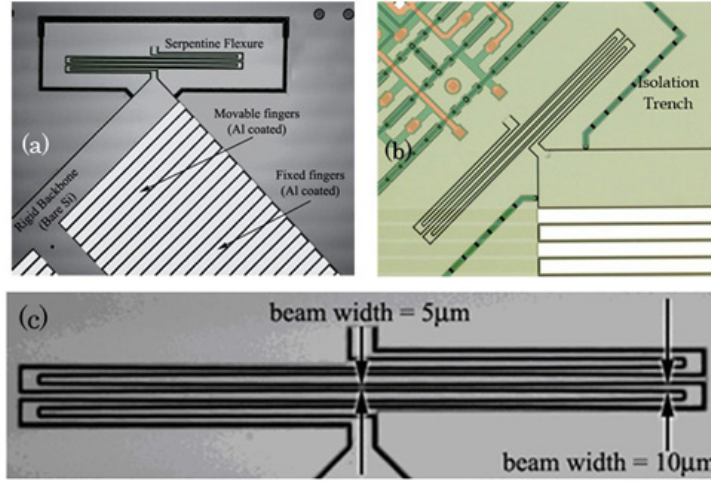


Figure 4.18: Microscope image of the fabricated device. The high-reflectivity light color areas are the aluminum coated reflective comb fingers; other areas are bare silicon. (b) Close-up view of the serpentine flexure; non-uniform beams cross section have a width of $5\text{ }\mu\text{m}$ and $10\text{ }\mu\text{m}$ at the center and the ends of the beam, respectively

the equation of motion for the device can be written as [74].

$$I_m \frac{d^2 z}{dt^2} + b \frac{dz}{dt} + K_f z = F(z, t) \quad (4.39)$$

where, I_m is the inertial mass, b is the damping constant, K_f is the mechanical stiffness, and F is the time and position dependent force, which can be expressed as;

$$F(z, t) = N_c \frac{dC}{dz} V(t)^2 \quad (4.40)$$

where N is the total number of comb fingers on one both sides of the device, C is the capacitance of a single finger, and $V(t)$ is the drive voltage. Figure 4.19 shows capacitance-displacement and rate of change of capacitance with respect to displacement for a single finger of a vertical comb actuator obtained using FEMLAB. Despite the displacement independent force of in-plane mode comb drives, the torque induced by the out-of-plane mode comb-actuators is strongly dependent on the displacement. As is discussed in detail in C, if the force expression in 4.40 is expanded as a Taylor series and the excitation signal is chosen to be a squared-root sinusoid ($V(t) = A\sqrt{\cos(\omega t) + 1}$) for mathematical convenience, the force induced by the comb-actuator can be expressed as;

$$F(z, t) = -A^2 \left(r_1 z + r_3 z^3 + r_5 z^5 + \dots \right) (\cos(\omega t) + 1) \quad (4.41)$$

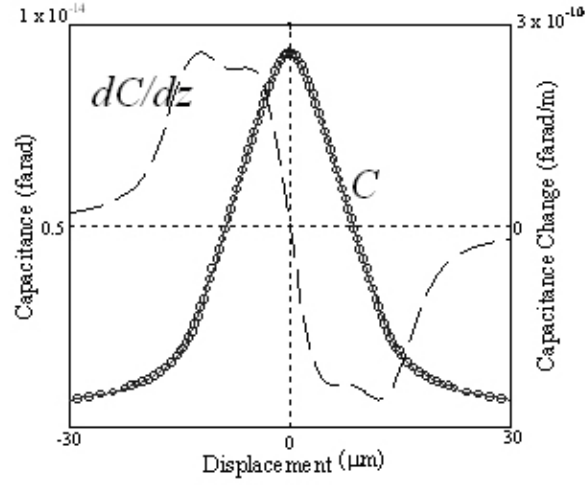


Figure 4.19: Capacitance vs. deflection curve and its derivative for a single comb finger. A high order polynomial is fitted to the FEA simulation data of capacitance curve. (Capacitance vs. deflection characteristics was simulated with FEMLAB)

where r_i are the Taylor series coefficients of the deflection-derivative of the capacitance function, which has only odd terms. Thus, the equation of motion becomes:

$$I_m \frac{d^2 z}{dt^2} + b \frac{dz}{dt} + \left(K_f + r_1 A^2 (\cos(\omega t) + 1) \right) + \left(r_3 A^2 (\cos(\omega t) + 1) \right) z^3 + \dots = 0 \quad (4.42)$$

The right hand-side of this equation has no constant force term; hence the system does not respond to DC excitation. This can also be observed from the symmetry of the structure in Figure 4.18. The time-dependent excitation of the equation of motion appears as the coefficients (parameters) of the equation of motion, which leads to time-modulation of the stiffness and nonlinearity terms. Such systems are called *Parametrically Excited Systems* and exhibit more complicated dynamic behavior compared to linear second order systems. A small parametric excitation can produce a large response when frequency of the excitation is close to twice the natural frequency (f_r) of the system (principal parametric resonance) or an integer fraction of $2f_r$. The resonance around $2f_r/n$ is called the n^{th} order parametric resonance. Hence, the excitation signal frequency should be around twice the mechanical eigenmode frequency (2.2 KHz in this case) in order to obtain maximum deflection from the device. Dynamic behavior of parametrically excited MEMS systems are analytically examined in detail in Appendix C.

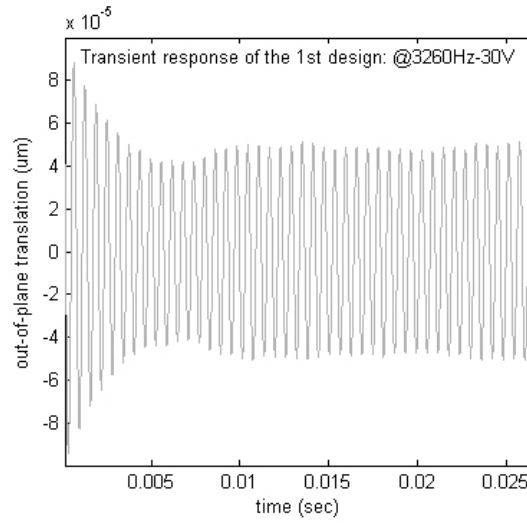


Figure 4.20: Transient response of the spectrometer. At 30V, steady state travel range is $\pm 52 \mu\text{m}$. Due to the displacement dependant force of the comb-actuators, the resulting system is a parametric oscillator.

As depicted in Appendix C, an analytical solution to 4.39 is not possible due to the complex nature of the forcing function, and one needs to make restrictive assumptions and simplifications for any analytical treatment. However, numerical modeling do not suffer from such limitations, and all types of system nonlinearities can be incorporated into the model. The main objective of performing numerical simulations on the dynamic behavior of the device is to approximate the driving signal amplitude that would lead to reasonable travel ranges. A numerical model of the system is developed with MATLAB. A Lorentz function is fitted to the capacitance vs. deflection curve, and this fit is incorporated into the model for accurate prediction of time and displacement dependent actuator force. The author has developed a similar numerical model for a torsional comb actuated microscanner for his M.Sc. thesis. Figure 4.20 shows the transient response of the spectrometer at the peak of the primary parametric resonance. Simulation result shows that a sinusoidal excitation of 30 V amplitude provides a travel range of $\pm 52 \mu\text{m}$ at steady-state. This provides sufficient OPD for moderately good spectral resolution (40 cm^{-1}) and is a very reasonable result. The mechanical Q-factor of the device is assumed 20. The simulations were carried out by numerically solving 4.39.

Frequency Response

Figure 4.21a shows the experimental frequency response within the principal parametric resonance range with sinusoidal excitation, and Figure 4.21b shows the excitation and oscillation waveforms at the peak of the resonance curve for square-wave excitation. Effects of the parametric nature of the system can be observed from both figures. The principal resonance is located around twice the natural frequency of the z-translation mode even though the actual device oscillates at its eigenmode; this phenomenon is called subharmonic oscillation and is a characteristic of parametrically excited systems. Moreover, the frequency response of the spectrometer shows a hysteretic behavior. Amplitude of the oscillations makes sudden jumps at two different frequencies. Oscillations between the two jump frequencies can only be excited if the external frequency is quasi-statically swept down to this region from above. Similar system dynamics are also observed in torsional comb-actuated microscanner devices and were studied in detail in [74], [106], [107]. At the peak of the frequency response curve, the device is capable of producing $\pm 53 \mu\text{m}$ vertical deflection with 28 V sinusoidal excitation, which is in very good agreement with the numerical simulations. However, for stable operation, the device should be operated slightly to the right of the resonance peak, since electrical or mechanical impulses might cause oscillations to vanish [74]. Similar devices were tested in open-loop for raster-scanning display applications and the oscillations were found to be stable [108]. A closed-loop system with position sensing mechanism can be used for better frequency and phase stability.

Amplitude Response

The experimental dependence of oscillation amplitude on the excitation voltage amplitude is plotted in Figure 4.22. Contrary to the in-plane comb actuation, this dependence very linear. This behavior can be analyzed using an energy method. Let's assume that the position of the mirror is represented with the function $x(t)$. Then, the damping force can be represented as

$$F_d = b(x)\dot{x}(t) \quad (4.43)$$

where, b is the damping coefficient. Assuming that the damping is constant with position, the total energy dissipated due to the damping over a single period of the mirror oscillation

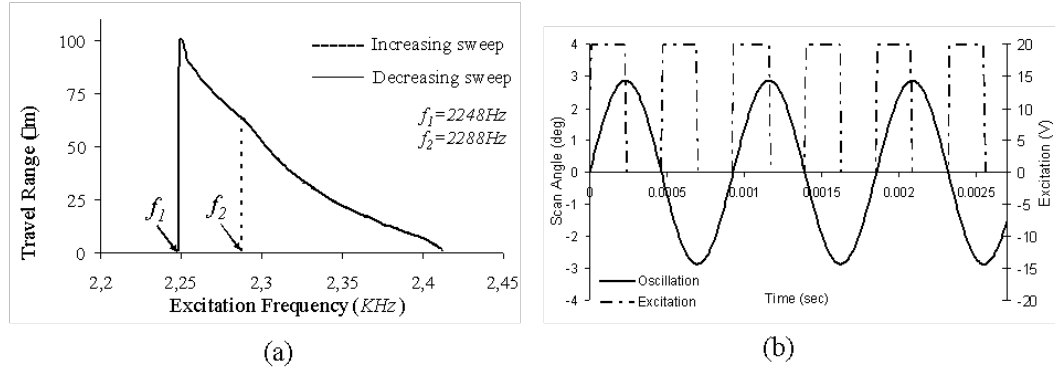


Figure 4.21: (a) Hysteretic frequency response of the spectrometer with 28 V sinusoidal excitation within the primary parametric resonance range. The actual device oscillations are at half the excitation frequency. Maximum amplitude is observed at the first jump frequency f_1 , which is equal to about twice the eigenmode frequency. (b) Excitation vs. Device oscillation at the peak of the resonance curve. Excitation signal is chosen as a biased square wave for better observation of subharmonic oscillations.

can be computed as

$$W_d = \int_T F_d x dx = \int_T b \dot{x}(t) x(t) dx \quad (4.44)$$

Introducing the change of variable $u = x(t)$, one gets

$$du = \dot{x}(t) dx \quad (4.45)$$

Hence, the dissipated energy becomes

$$W_d = \int_T b u du = \left[\frac{b u^2}{2} \right]_{-T/2}^{T/2} = \left[2 \frac{b x^2(t)}{2} \right]_{-T/2}^0 \quad (4.46)$$

For the steady state resonant motion, the mirror oscillation can be assumed to be purely sinusoidal as in the form $x(t) = a \cos(2\pi t/T)$, where a is the oscillation amplitude. In this case, the energy dissipated by the damping in a single period can be expressed as

$$W_d = \left[b a^2 \cos^2 \left(\frac{2\pi t}{T} \right) \right]_{-T/2}^0 = b a^2 \quad (4.47)$$

For a steady state oscillation, the amount energy transferred into the system by the actuator in a single period is equal to the dissipated energy.

$$W_e = W_d = b a^2 = 2 \frac{1}{2} C V^2 \quad (4.48)$$

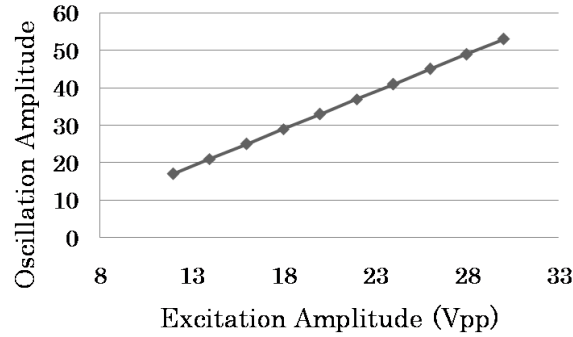


Figure 4.22: The linear dependence of maximum oscillation amplitude on the excitation signal amplitude .

This yields,

$$a = \sqrt{\frac{C}{b}} V \quad (4.49)$$

where, C is the total capacitance of the mirror at rest position, and the mirror is assumed to be driven with a raised square wave signal with peak-to-peak amplitude V . Therefore, the oscillation amplitude, and the driving voltage amplitude are linearly dependent.

Mechanical Robustness

The shock response simulations is performed with ANSYS, and the results are shown in Figure 4.23. The device was assumed to be under a shock of 2000g for duration of 600 μsec , which is approximately one period of the out-of-plane translation mode (worst-case scenario). The shock force was assumed to be uniformly distributed over the top surface in the perpendicular (z) direction. Maximum deflection after the shock was applied turned out to be 270 μm . Maximum deflection was occurred at time 330 μsec , and the stress distribution on the device at this time is shown in Figure 4.23b. As expected, the stress is concentrated on the corner of the folds. A maximum stress of 1.25 GPa was observed at maximum displacement, which is within the stress limits of silicon flexures.

Optical Performance

Figure 4.24 shows the simulation results of separation and the spot-size of the diffraction orders. Figure 4.24a plots the intensity profile of the diffraction pattern when light is

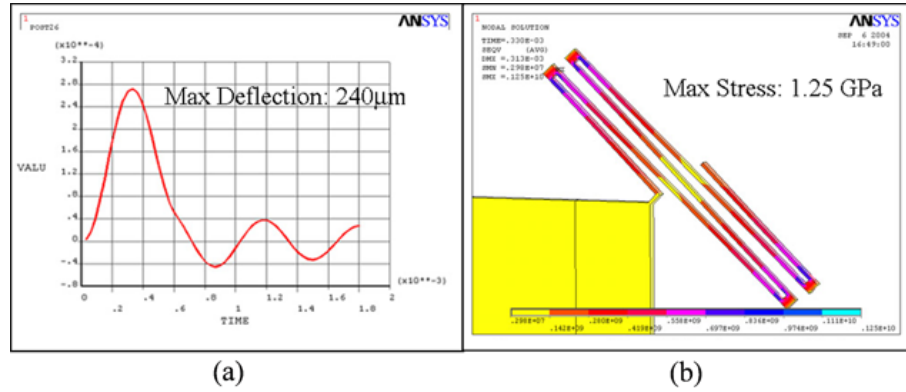


Figure 4.23: (a) z-component of the transient response of the device to a shock wave of 2000g, which is of duration 600 μsec . Quality factor was assumed to be 20. Maximum deflection occurs at time 330 μsec and equals to 270 μm . Since the shock pressure was distributed evenly on the surface of the device, the movement is pure vertical sliding. (b) Stress distribution on the flexures at maximum deflection. Maximum stress is equal to 1.25 GPa, and occurs at the exact corners of the folds. Rounding the corner of the folds would significantly reduce the maximum stress and therefore enhance shock resistance of the device.

diffracted from only two periods of the grating, where in Figure 4.24b, ten grating periods are illuminated. For both cases, diffraction angle is constant and equals to 7 mRad, but the spot sizes are significantly different. As expected, the spot-size of the diffraction orders shrink as the number of illuminated grating periods increase. Light efficiency of the grating can be defined as the ratio of the total light energy reaching 0th order to the total light energy incident on the grating at the undeflected (zero OPD) state, where the grating acts as a simple mirror. Theoretically, the grating efficiency is computed to be 80%, due to the light lost at the finger gaps, and the diffraction created by them. However, experimental diffraction efficiency of the device is measured to be 15%, which is much lower than the theoretical value. Figure 4.25 shows the diffraction profile at zero OPD state, which has significant energy at 1st orders. This is due to the stress-induced bow of the long and thin comb fingers. Due to this bow, a built-in height difference between the comb fingers are present, and this yields to significant diffraction, even in the static case. The reason behind the static deflection of the grating fingers is the thermal stress. The metallization process (Al evaporation) is done at room temperature; however, at the surface of the wafer, the temperature goes up to 80C due to the heat generated by atomic collisions. After the

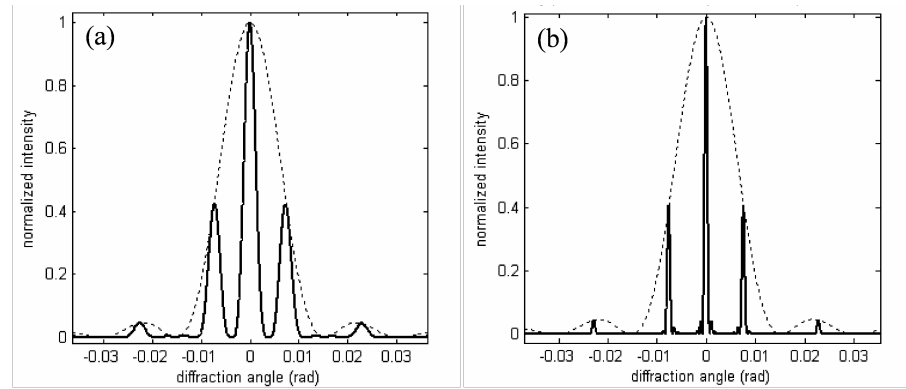


Figure 4.24: Separation of the diffraction orders on the diffraction plane. (a) 2 periods of grating are illuminated; (b) 10 periods grating are illuminated. For both figures, the angle between the diffraction orders is 7mRad

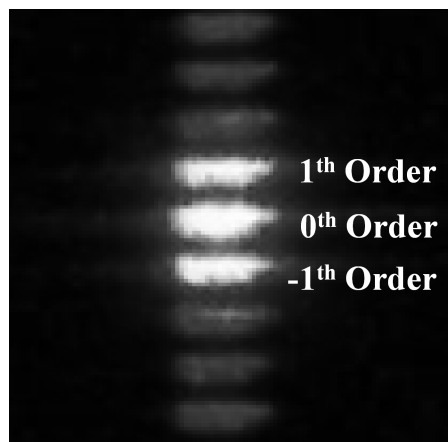


Figure 4.25: : Diffraction efficiency of the spectrometer system. (a) Experimental diffraction profile for zero OPD. Efficiency is %15, which is significantly lower than the theoretical value.

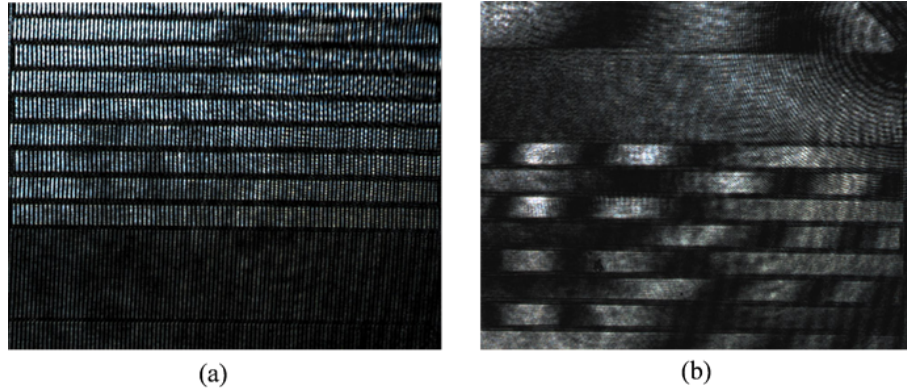


Figure 4.26: a) Interferogram with a spatial carrier (b) 3D surface profile of the fingers. Peak deformation is 490 nm.

process, the device cools down to the room temperature and the thermal stress is induced due to the mismatch between the thermal expansion coefficients of silicon and aluminum. Formulas for thermal deformation for such two layer structures can be found in the literature [109]. Using the expression in that reference, the static deformation of the fingers is found to be 411 nm at the tip, which is more than enough to degrade the efficiency. To measure this deformation, a custom laser interferometer was built. An interferogram obtained with this setup and the computed surface profile are shown below.

4.3.5 Spectral Measurements

Spectral measurements are performed with a single photodetector placed at the 0^{th} order. Due to the resonant motion of the device, the speed of the movable fingers is not constant, but sinusoidally varying. Therefore, the recorded detector intensity for a monochromatic source is a chirped (i.e. varying frequency) sinusoid. Figure 4.27a shows this chirped modulation of 0th order intensity for a half-period movement of the fingers. The maximum travel range is chosen to be very small ($1.45 \mu\text{m}$), so that the modulation frequency is low and the chirping effect is clearly visible. Frequency of the chirped signal is the lowest at the extremities of the deflection where the velocity is low. The actual cosine modulation of the intensity with respect to the finger position $I(\rho)$ is plotted in Figure 4.27b.

$I(t)$ and $\rho(t)$ given in Figure 4.28a are obtained by an A/D conversion which samples the continuous intensity and the position signals at uniform time intervals. However, $I(\rho)$

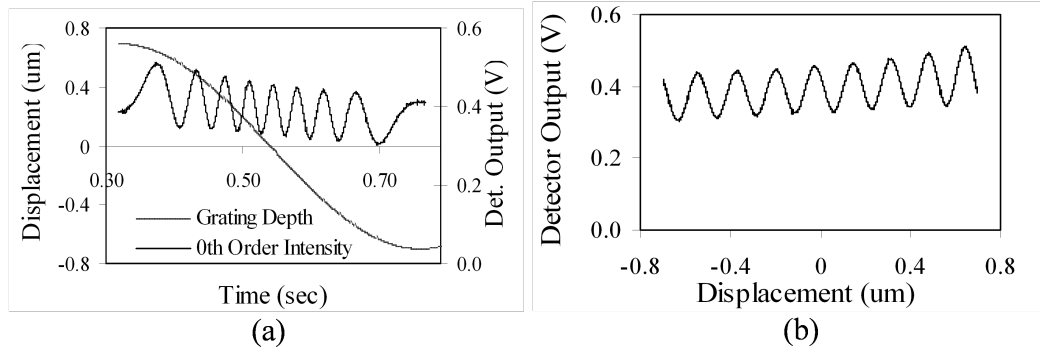


Figure 4.27: a) Modulation of the 0th order intensity w.r.t. time with monochromatic illumination (experimental data). b) Modulation of the 0th order intensity w.r.t. finger position.

of Figure 4.28b consists of non-uniformly spaced samples. In order to obtain a uniformly sampled $I(\rho)$ data, an interpolation and resampling algorithm is used before the Fourier transform operation to obtain the spectrum. An alternative is to use a non-uniform sampling clock which can be generated using the electronic drive signal or using the optical feedback obtained by an additional monochromatic source and a detector, which provides a simple and elegant solution to the problem. Two different interferogram smapling methods based on this principle are investigated in the next subsection. Figure 4.28a plots experimental detector output vs. optical path difference data. Measured source was a red laser diode and total travel range of the fingers was limited to $\pm 9.5 \mu\text{m}$. In Figure 4.28b the spectrum of the light measured with the FTS and a commercial spectrometer are compared. The FTS result is obtained by the Fourier transforming of the data in Figure 4.28a. Sinc shape of the measured peak is due to the rectangular window of the Fourier transformed signal data as given in 4.34. The peak wavelength is measured to be at 639 nm with the FTS, while the reference measurement result is 638nm. High resolution position feedback of the vibrometer ensures that the peak wavelength is located within a good accuracy. However, the measured FWHM is 12.2 nm, and it is slightly more than twice the theoretical value (5.8nm). Light collection efficiency is measured to be 15%, which is significantly lower than the theoretical value.

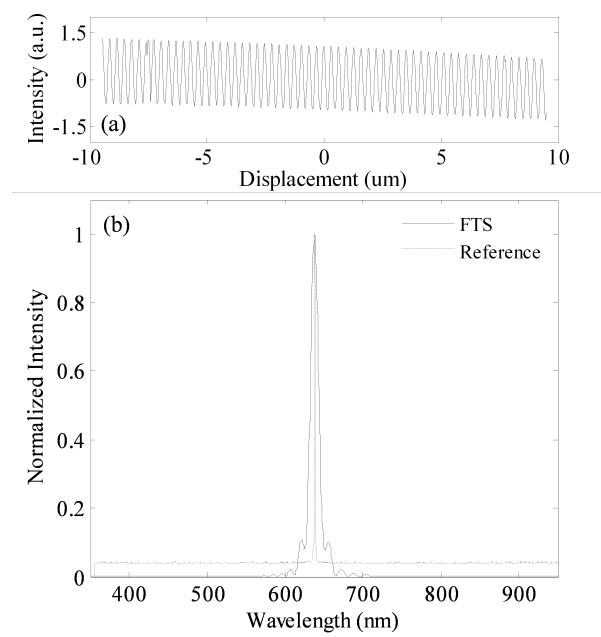


Figure 4.28: a) Experimental detector signal vs. optical path difference for a travel range of $19\ \mu\text{m}$ b) Comparison of spectral measurements performed with the FTS and a commercial spectrometer.

4.3.6 Discussion

Design, fabrication and characterization of an out-of-plane vertical comb-drive actuator based Fourier transform spectrometer with $3\text{mm} \times 3\text{mm}$ clear optical aperture is discussed. The device is capable of producing $\pm 52\ \mu\text{m}$ vertical deflection at 28V square-wave excitation without requiring vacuum packaging. This performance level yields a theoretical spectral resolution of about $0.4\ \text{nm}$ in the visible band and $3.6\ \text{nm}$ at telecom wavelengths. The device is also suitable for spectroscopy in the mid and the long wave IR bands. The device fabrication is simple and uses standard SOI process with 3 masks and does not require additional IR optics. The only moving part is the resonating MEMS structure. Since the comb-fingers are used both for actuation and as dynamic diffraction grating, the die is not much larger than the optical aperture. This minimizes the size, complexity, cost, and the design effort of a fully functional system. An operational spectrometer using this die requires integration with a simple photodetector and associated readout circuitry. The spectrometer has a number of unique features and important advantages compared to other MEMS spectrometers discussed earlier.

4.3.7 2nd Generation Devices

A set of MEMS spectrometer structures were designed, fabricated and tested, as discussed in the previous subsection. The devices were operational, and proved well the validity and novelty of the comb actuator based Fourier spectroscopy approach. However, static bending and dynamic deformation of the fingers due to thin structural layer, and moderate travel range made an updated new run necessary. Hence, a 2nd set of MEMS spectrometer devices were designed. The new designs share the same basic features with the devices of the 1st run, but are optimized to solve the problems associated with them. Figure 4.29 is a schematic drawing of basic device structure shared by all design variations. Mechanical optimization of new structures are performed using finite element analysis simulations, and analytical modeling of dynamic characteristics are investigated through analytical methods that were developed during the author's masters thesis project. The fundamental structural modifications are listed below:

- Device layer thickness is increased from 30 μm to 100 μm , to prevent static and dynamic finger bending, which limited optical efficiency and maximum achievable deflection without compromising surface quality.
- Grating period reduced to 45 μm from 145 μm to enhance diffraction order separation and increase the number of fingers within a fixed area leading to a higher force density.
- In order to keep the resonance frequencies around 1 KHz despite the increase in the device thickness, the folded flexures are thinned down to 30 μm with an additional partial DRIE etch. Alignment between the two DRIE steps adds a complexity to the fabrication process that did not exist before.
- Coating material is switched to gold from aluminum enabling us to electroplate magnetic thin films on top of the device and explore an alternative electromagnetic actuation method.

Using the bimaterial thermal bending formulas from [109], static bending for the new designs are computed as 14.5 nm and 25.7 nm for the 750 μm , and 1000 μm fingers, re-

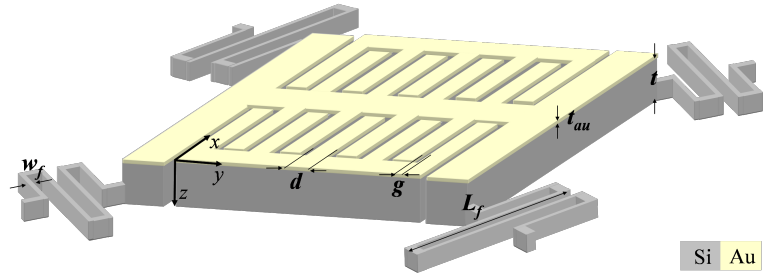


Figure 4.29: device structure. (1) Fixed Fingers, (2) Movable Fingers, (3) Rigid Backbone, (4) Folded Flexures. (The figure is not drawn according to actual proportions.)

spectively. For the NIR range, the expected static bending is in the order of $\lambda/50$, which is sufficiently low for satisfactory optical performance.

4.3.8 Interferogram Sampling

The interferogram should be recorded as a function of the OPD; hence an optical position feedback mechanism for the grating is necessary. Position monitoring is an important and common issue for MEMS based systems. Various solutions have been proposed for this problem, including capacitive, piezoresistive, and optical methods. From fabrication and accuracy aspects, optical methods provide the best performance, and is implemented for the proposed system [110]. Hence, an optical position feedback mechanism for the FR4 platform is implemented. Position feedback and sampling clock signal is obtained using the backside of the FR4 platform and the fixed grating referred to as feedback grating. A visible laser diode (LD) in the visible band can be used to generate a non-linear sampling clock signal by using the zero crossing or the peaks of the photo detector (PD) signal. This is essentially a secondary lamellar grating interferometer with a monochromatic source placed at the backside of the mirror. If the mirror velocity is constant, temporally uniform sampling of the interferogram ensures the spatial uniformity of the device. However, in AC operation, due to the sinusoidal speed variation of the mirror, uniform temporal sampling of the interferogram corresponds to non-uniform spatial samples, on which Fourier transform is inapplicable. Sampling the interferogram at the zero crossings of the laser reference signal from the feedback grating produces spatially uniform samples due to the precise $\lambda/4$ displacement of the platform between each zero-crossing. A very similar method is

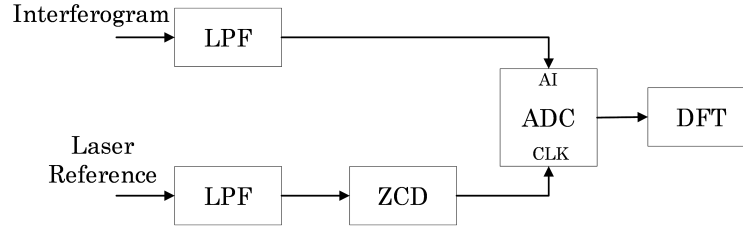


Figure 4.30: Chirped reference laser interferogram and associated clock signal produced by a zero-crossing detector

commonly used for conventional FTIR systems, but the feedback interferometers are almost always of Michelson type. Two different methods for the detection of the zero-crossing times of the reference laser interferogram is designed and implemented.

Sampling with Chirped Clock

The first interferogram sampling method implemented here featured a zero-crossing detector to produce a non-uniform clock signal to trigger the A/D converter to sample the interferogram at uniform spatial intervals, as shown in Figure 4.30. An experimental chirped feedback signal and the associated clock signal are shown in Figure 4.31, for 4 μm mirror translation.

Conventional A/D converters are triggered either at the falling or rising edge of the clock signal. This translates into one sample per reference interferogram period; hence, the Nyquist criteria dictates that the low wavelength cut-off is at twice the reference laser wavelength. To extend the low cut-off down to the reference wavelength, both the rising and falling edges of the clock signal should be utilized. A Silabs microprocessor evaluation board with two A/D converters, built in memory and two op-amps has been programmed to implement this interferogram sampling method. The fundamental problem associated with this method is the electronic processing delay of the zero-crossing detector, that might lead to significant sampling time errors, especially for rapid scanning.

Interpolated ZCD Sampling

An elegant interferogram sampling method was developed by [111] in order to overcome the sampling delay issue in Fourier spectrometers that we adapted to and implemented for

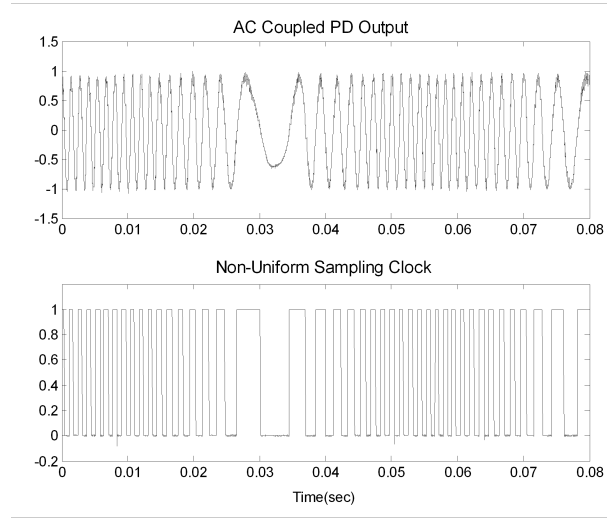


Figure 4.31: Chirped position feedback signal and the spatially uniform/ temporally non-uniform clock signal produced by a zero-crossing detector

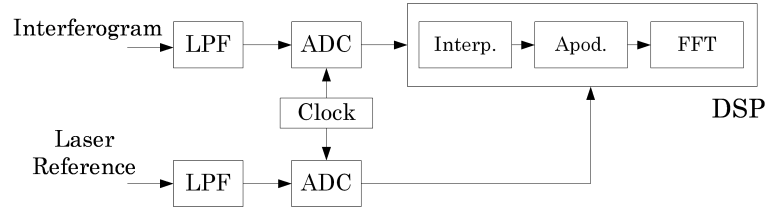


Figure 4.32: Block diagram representation of the interferogram sampling method. Both the interferogram and laser fringes are sampled; zero crossings of the laser fringes are found by interpolation.

our system. In this system, both the laser fringe and the interferogram are sampled with separate ADCs synchronized by a single high frequency clock signal (Figure 4.32). The zero crossing times of the laser fringes are computed with a linear interpolation step, followed by a cubic interpolation of the interferogram to compute its value at the computed zero crossing time. This method requires a faster ADC than using the output of a zero-crossing detector as a sampling clock for the ADC; but the sampling time errors that may arise due to electronic delays can be eliminated completely (Figure 4.33)

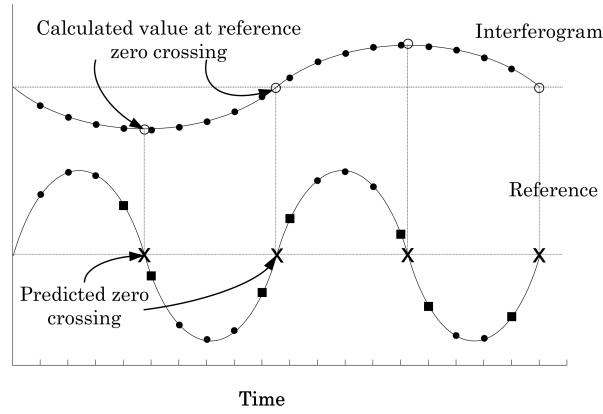


Figure 4.33: Illustration of sampling timing scheme of the implemented interferogram sampling method.

4.4 FR4 Optical Platforms for Milimeter Scale Spectrometers

In addition to the electrostatic comb actuated MEMS LGI approach, an alternative technology was also explored for FTIR spectroscopy that has certain inherent advantages and competes well with the MEMS approach. In this section, two compact FTIR systems in Michelson interferometer and lamellar grating configurations, employing a novel electromagnetically actuated FR4 scanning platform are presented. FR4 is a polymeric epoxy-glass resin commonly used as a substrate material for printed circuit boards (PCB) found in almost all electronic equipment. Therefore, the material has well-known electrical, thermal and mechanical properties and the fabrication technology is widely available at low-cost. There have been numerous attempts at integrating new functionality into PCB technology through novel usage of the FR4 substrate [113], [114]. Our group at Ko University was the first to utilize FR4 as an electro-mechanical platform with integrated electrical and optical functionalities [115], [116]. In this work, the use of this novel platform is extended into FTIR spectroscopy.

In subsection 4.4.1, the fundamentals of FR4 mechanics - material properties, fabrication, and actuation - are presented. A lamellar grating interferometer employing a FR4 platform and a new method of optical position feedback are discussed in 4.4.2. Subsection 4.4.3 presents a high performance Michelson type FTIR spectrometer with a FR4 platform bearing a retro-reflector. A discussion on the improvement of motion linearity and

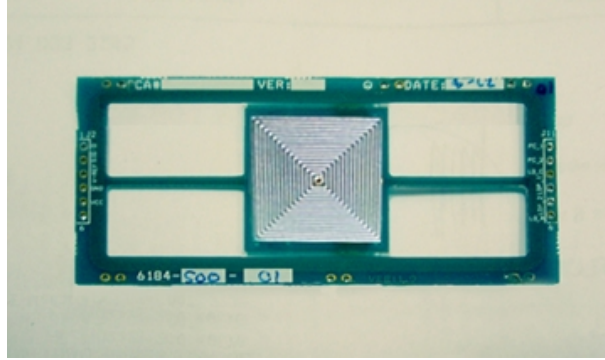


Figure 4.34: Photograph of the conventionally machined FR4 scanner with routed coils for EM actuation

implementation of other spectroscopy methods with the presented platform are given in 4.4.4.

4.4.1 FR4 as a Mechanical Material

Crystalline silicon is proven to be an excellent structural material for high performance microsystems due to its exceptional mechanical properties. However, crystalline silicon is very stiff and brittle; thus, low frequency (in the order a few hundred Hz) MEMS structures become very delicate and cannot survive the shock and vibration in the environment. FR4, having a low Young modulus of about 20 MPA, is inherently a soft material and a good candidate for low frequency scanning applications, which usually are very challenging for silicon MEMS devices. Moreover, the electrical circuitry required to drive the FR4 mechanical elements can easily be integrated on the same circuit board with additional optic/optoelectronic components, using conventional PCB manufacturing equipment. A drawback of FR4 based mechanics as a result of conventional machining is the relatively low structural precision compared to microfabricated MEMS. The FR4 platforms presented in this paper have a minimum linewidth and spacing of $125\ \mu\text{m}$ for the coil and a minimum mechanical feature size of $500\ \mu\text{m}$ with a precision in the order of $100\ \mu\text{m}$.

Figure 4.34 is a photograph of the FR4 movable platform carved from a printed circuit board with $130\ \mu\text{m}$ thick polymer and $35\ \mu\text{m}$ thick copper layers on both sides. On either side of the central 8 mm by 8 mm square plate, the copper layer is scraped to form a single electromagnetic coil connected through two vias. This plate is linked to the outer

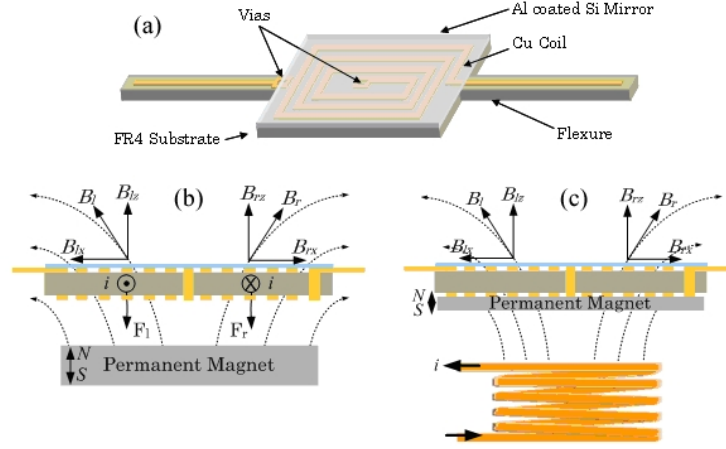


Figure 4.35: (a) Schematic drawing of the platform with attached reflective mirror. Copper coils rest on either face of the platform. (b) Electromagnetic moving coil actuation of the FR4 platform with a rectangular or circular permanent magnet symmetrically placed underneath. (c) Electromagnetic moving magnet actuation with a permanent magnet attached to the platform and an external EM coil symmetrically placed underneath. For both actuation methods, lateral component of the magnetic field and the direction of the coil current are in opposite direction, creating a net force on the platform in the vertical direction [117].

frame through two flexure beams in a simple torsional scanner form, which also carry the electrical feed-throughs for the coil (Figure 4.35a). For optical functionality, an aluminum coated silicon mirror is mounted on the front and back side of the central plate.

4.4.2 Electromagnetic Actuation of FR4 scanners

FR4 platforms are actuated with electromagnetic Lorentz force, allowing both DC and AC operation. A rectangular or circular permanent magnet is symmetrically placed underneath the platform creating a magnetic field B . A current i passing through the coils under this magnetic field induces a mechanical force that vertically translates the platform ($F = B \times i \times l$). Figure 4.35b is a schematic drawing summarizing the actuation principle. On either side of the platform, lateral component of the magnetic field and the direction of the coil current are in opposite direction, creating a net force on the platform in the vertical direction. The vertical components of the field lines create lateral forces in opposite direction on either side of the platform that cancel each other. Due to the large dimensions of the platforms, a significant amount of electromagnetic force can be generated by the actuators, leading to a large travel range, which is not easily achievable with MEMS structures. Depending on

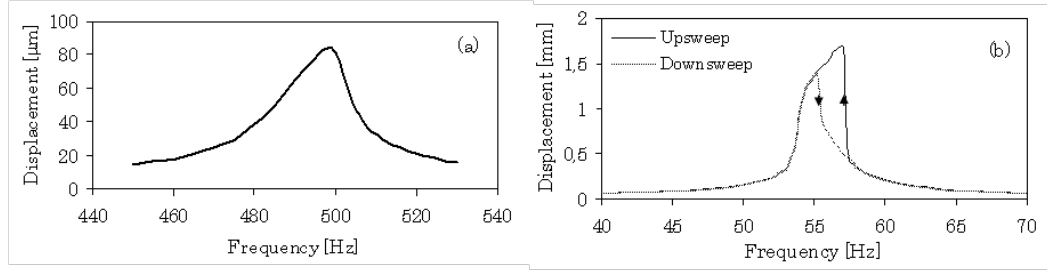


Figure 4.36: Frequency response of the FR4 platform in the (a) moving coil, and (b) moving magnet configurations, measured with a position sensing detector. External coil and device coil currents are 16mA, and 10mA, respectively. The hysteretic resonance behavior is due to the spring stiffening.

the orientation of the magnetic field and the direction of the current, the platform can be moved in torsional or translational modes, making the device useful for various scanning applications.

Measured frequency response for the out-of plane (z) translation mode of the platform with moving coil actuation is shown in Figure 4.36. The experiment was performed with a permanent magnet creating 0.1 Tesla on the coil and a drive-current of 10mA. The system behaves like an ordinary 2nd order system with a resonance frequency of 498 Hz and resonance deflection of 82 μm . In moving magnet actuation, the behavior is very similar, but the resonance frequency is significantly lower due to the additional mass of the attached magnet, and the maximum achievable deflection is higher due to stronger EM force. As can be seen in Figure 4.37, the scan angle increases linearly at low drive currents, and its rate slowly drops due to the spring stiffening effect at higher drive amplitudes.

4.4.3 Lamellar grating Interferometer with FR4 platform

Figure 4.38 is a schematic drawing of the LGI based Fourier transform spectrometer employing an FR4 platform as the movable component. An aluminum coated silicon grating is placed on top of the FR4 platform to form the static fingers of the movable grating required for the LGI system. The 30 μm thick silicon grating has a period of 140 μm and 50% duty cycle (the finger width and the gap between the fingers are both 70 μm). A secondary grating is placed underneath the FR4 platform for an optical position feedback system that will be discussed in the next sub-section.

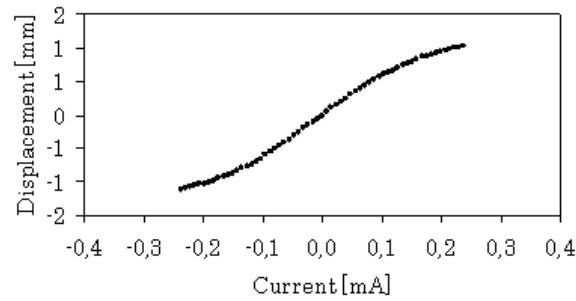


Figure 4.37: Drive currentMechanical response linearity of the platform measured in the moving magnet configuration. Magnetic field intensity remains constant within the displacement of the platform; hence, the deviation from the linear behavior is due to the spring stiffening

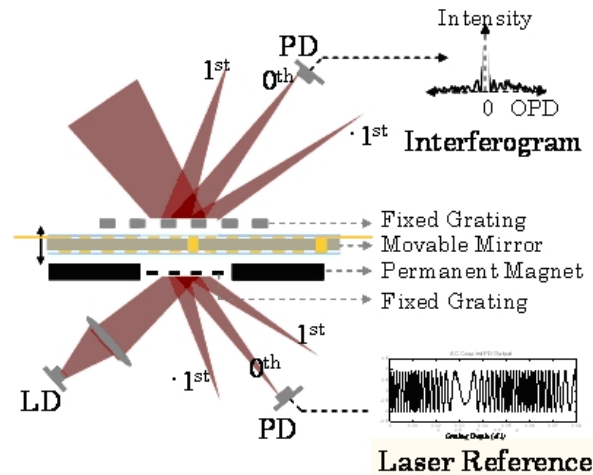


Figure 4.38: Schematic drawing of the lamellar grating interferometer employing a FR4 platform.

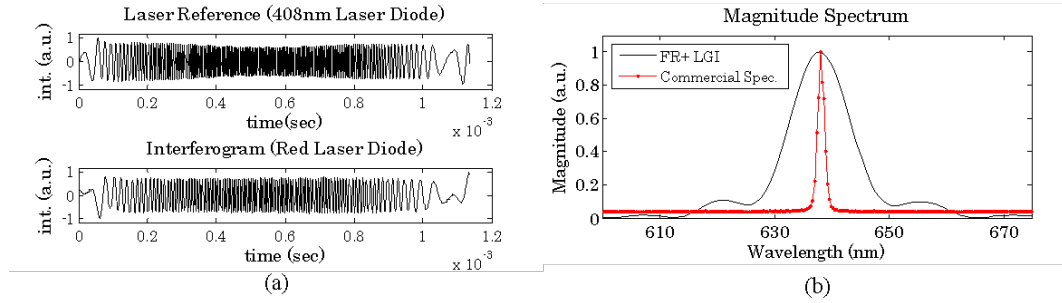


Figure 4.39: (a) Experimental reference laser fringes and the interferogram. Peak-to-peak mirror displacement is $22 \mu\text{m}$ (Off-resonance moving coil actuation at 10 Hz). Both the laser reference and the interferogram are chirped, due to the sinusoidal speed variation (b) Calculated spectrum and comparison with a commercial grating spectrometer result. FWHM resolution is 7 nm. Peak wavelength accuracy is better than 0.1%.

There are two fundamental problems with the presented lamellar grating interferometer system. The net OPD for light reflected from the mirror and the grating is always positive due to the finite thickness of the grating structure (minimum OPD is $30 \mu\text{m}$ for the presented system). The effect of this is equivalent to high-pass-filtering of the spectrum, where the cutoff frequency is determined by the lowest value of the OPD. Hence, the slowly varying portion of the spectrum is omitted, but the sharp spectral lines remain intact. Second problem associated with this method is related to the fabrication tolerance. The moving grating is the heart of the system and the tolerances on motion precision are tight (i.e., the optical path difference along the clear aperture of the mirror should not exceed $\lambda/4$ where is the smallest measured wavelength). The FR4 scanners are fabricated via conventional machining; hence all corners of the mirror are not translated by the same amount, particularly for large deflections, resulting in significant distortion of the acquired interferograms.

Figure 4.39 presents the experimental laser reference and interferogram data obtained with the LGI setup. For reference interferogram, a blue-violet laser diode (408 nm) was used. For experimental simplicity, a red laser diode is used for the spectrum to be measured, instead of an IR source. Maximum travel range of the FR4 movable platform is $22 \mu\text{m}$, yielding a spectral resolution of 2 cm^{-1} . The limiting factor on the mirror displacement was motion linearity. Beyond $22 \mu\text{m}$, interferogram and laser reference signal are significantly deteriorated due to mirror wobbling.

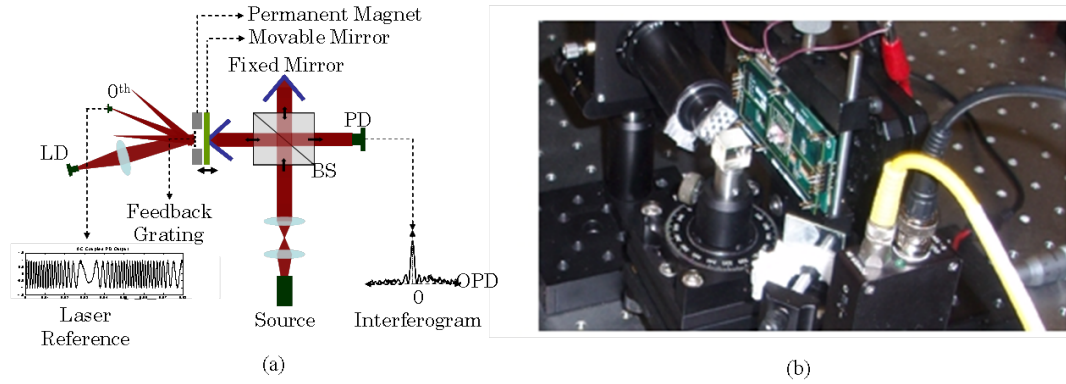


Figure 4.40: Fourier spectrometer in Michelson configuration. (a) Overall setup with two laser sources: 408nm laser diode for laser reference, 637nm laser diode as measured source. (b) Basic interferometer unit and the FR4 scanner bearing a retro-reflector

4.4.4 Michelson Interferometer with FR4 Platform

The most common form of Fourier transform spectrometers is the Michelson interferometer with a moving mirror. In this configuration, the light beam from the measured source is amplitude splitted by a beam splitter and both arms reflected from two flat mirrors - one static, one movable. Then these arms are superimposed to interfere. An interferogram, which is mathematically identical to 0th order interferogram of an LGI, can be recorded as a function of the mirror displacement. The optical path difference between two arms of the interferometer can be set to zero; therefore, the entire interferogram can be recorded unlike the FR4 based LGI. A major disadvantage of this method compared to the LGI configuration is requirement of additional optical components (beam splitter, focusing optics) and tighter alignment tolerances.

A Michelson type FTS was built with retro-reflectors (corner cubes). A small and light-weight plastic corner cube with 50 nm evaporated aluminum layer on top was attached onto the FR4 platform. Retro-reflectors are an effective solution to correct for the wobbling problem, which are also widely used in bulk FTIR spectrometers to correct for mirror misalignments. Integration of retro-reflectors drastically improved the maximum allowable travel range of the FR4 scanners at a cost of lower cone angle, due to their small aperture size. A schematic drawing and a photograph of the built spectrometer are shown in Figures 4.40a and 4.40b, respectively.

Figure 4.41 plots experimental laser reference and interferogram signals acquired with the setup together with the associated computed spectrum. Maximum travel range of the platform is extended to $495\text{ }\mu\text{m}$ without distorting the interferogram, and the spectral resolution is improved to reach 20cm^{-1} . A triangular apodization applied on the interferogram to suppress the side lobes of the spectral peak which result from the rectangular instrumental function of the spectrometer. Figure 4.41c shows that the spectral resolution of the spectrometer is 0.89 nm at 635 nm , outperforming the commercial spectrometer used to record the reference measurement. The 2% wavelength accuracy is slightly worse than the LGI data, and is probably due to the thermal shift of the reference laser wavelength. This minor problem can be solved by temperature stabilization of the reference laser with a thermo-electric cooler.

Due to the poor surface uniformity of the plastic retro-reflectors used in the experiments, white light interferogram acquisition presented a significant challenge. Hence, the retro-reflectors were replaced again with flat aluminum coated silicon mirrors, and the travel range kept at $\pm 20\text{ }\mu\text{m}$ to minimize interferogram distortion. The recorded interferogram for a red LED and the associated spectrum acquired with these settings are given in Figure 4.42. The comparison of the measured spectrum with the reference spectrum shows that, even though the center frequency was correctly decoded by the spectrometer, the measured linewidth is almost twice the actual value (35 nm measured versus 20 nm actual FWHM linewidth). This discrepancy is not due to the low maximum deflection, since the theoretical resolution with $\pm 20\text{ }\mu\text{m}$ is 8 nm at 635 nm . Furthermore, the shape of the interferogram clearly shows that at the extremes of the interferogram, interferometric modulation disappears due to the low coherence length of the source. Hence, it can be claimed that the reason behind the broadening of the measured spectra is the distortion of the interferogram due to the discrepancy in mirror motion. Dynamic surface profile measurements for the mirror are necessary to fully explain this effect.

4.4.5 Discussion

Extension of the linear translation range of the platforms is essential to improve the performance of the spectrometer system. Current open loop operation of the platforms with a corner cube allows a travel range of half a millimeter without distorting the interferograms,

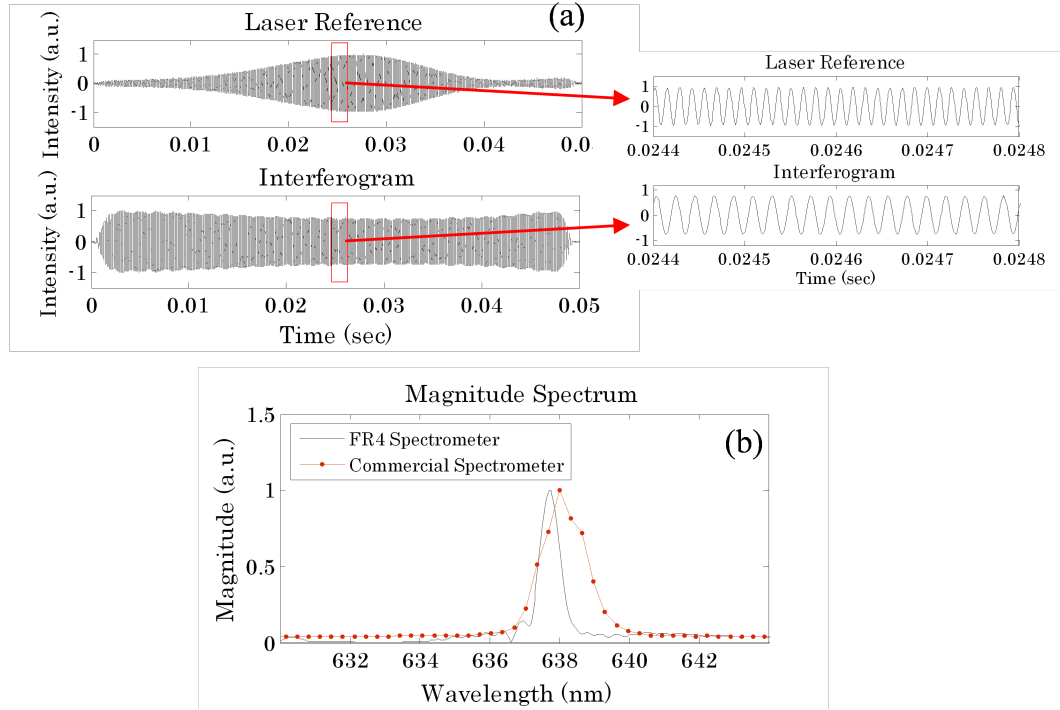


Figure 4.41: (a) Experimental interferogram and laser reference data acquired using the FR4 Michelson interferometer setup. Peak-to-peak mirror displacement is $496 \mu\text{m}$ (Off-resonance moving magnet actuation at 10 Hz). Both the laser reference and the interferogram are chirped, due to the sinusoidal speed variation (b) Interferogram and laser reference signals zoomed (c) Output of the FR4 spectrometer compared with a commercial grating spectrometer. The interferogram is triangularly apodized before Fourier transformation. Resulting apodized FWHM resolution is 0.89 nm and is better than the commercial spectrometer performance

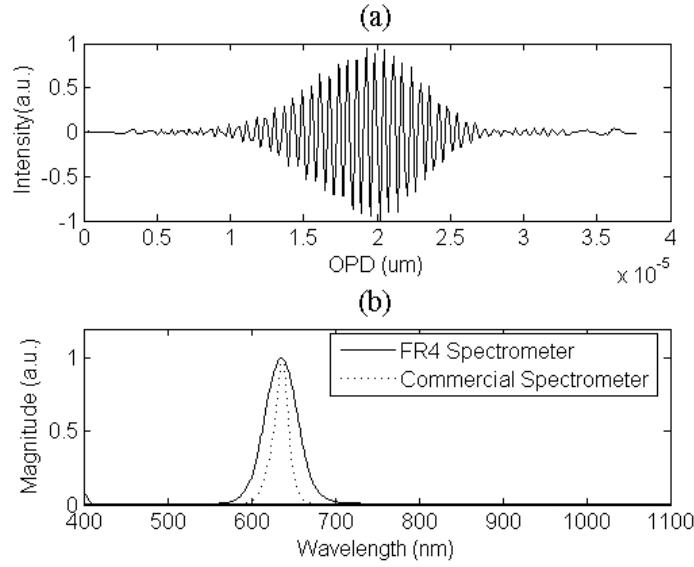


Figure 4.42: (a) Interferogram recorded with a red LED source (b) Associated measured spectrum compared with a reference measurement.

but beyond this range, the mirror wobbling becomes a problem for the current design. The linear motion range of the scanner can be extended using multi-point actuation and associated position feedback and close loop-control using a back-emf sensor coil.

Depending on the orientation of the magnetic field and the direction of the current, the platform can be moved in a torsional or translational mode, making the device useful for various scanning applications. In the torsional mode, the FR4 platform can also be used as a rotating grating spectrometer similar to the MEMS spectrometer in [18] with higher spectral resolution and lower electronics bandwidth requirements but still provide spectrum in a fraction of a second. For this purpose, instead of a reflective mirror, a micro-molded, aluminum coated polymer grating should be placed on top of the FR4 scanner. The classical grating spectrometer employs a static grating and a detector array placed at the 1st diffraction order. For all wavelengths, 0th diffraction orders are at the same position; but 1st diffraction orders are spread in space due to different diffraction angles. The detector array then can be used to image the light distribution around the 1st diffraction order. In a rotating grating spectrometer, a single detector is sufficient, since as the mirror rotates, entire spectrum is scanned over the detector in time.

Two compact Fourier transform spectrometer systems in Michelson interferometer and

lamellar grating interferometer are demonstrated using a vertically translating FR4 platform. An experimental spectral resolution of 20 cm^{-1} is shown for the Michelson interferometer. Real-time operation, long and controllable travel range, simple fabrication and easy integration are the major advantages of the proposed approach promising a great potential for development of compact spectrometer with good performance.

Chapter 5

COMB ACTUATORS FOR LASER SCANNING CAMERAS

Laser scanning is a key technology behind many modern high resolution display and imaging systems. Through the single source-single detector approach, laser scanning can significantly reduce the cost and the complexity of demanding applications, which traditionally employ costly detector arrays, and inefficient flood illuminators. Furthermore, fields like medical imaging diagnostics and measurement technology have increasing demand on compact and high performance imagers, as established in Chapter 2. Hence, miniaturization of high resolution scanning systems is crucial. In this chapter, presented is a novel microlens array based high-resolution laser scanning method that is specifically targeted for miniaturized laser based display or imaging systems. The scanning architecture consists of four cascaded micro-optical elements (1 microlens and 3 microlens arrays (MLA)), where the lens and one of the MLAs are laterally moving. A novel integrated micro-opto-mechanical platform is proposed for the implementation of the developed method, and partially fabricated. This novel platform proposed here will enable precision alignment and scanning of the micro-optical components through integrated fabrication of polymer micro-optics on silicon micro-mechanics.

This chapter consists of four sections. Section 5.1 presents the novel laser scanning method with optical analysis, and a set of proof-of-principle experiments. Next section develops the specifications for a one-dimensional micro-opto-mechanical platform targeted for the laser scanning system. Next two sections present the design, fabrication, and characterization of a novel microlens array component and a MEMS micro-optical scanner device, respectively.

5.1 Laser Scanning with Microlens Arrays

A novel microlens array based high-resolution laser scanning architecture has been developed within this thesis. The scanning architecture consists of four cascaded micro-optical

elements (1 microlens and 3 microlens arrays (MLA)), where the lens and one of the MLAs are laterally moving. In this section, a detailed description of this scanning method is presented with a comprehensive background on refractive scanning.

5.1.1 Background

The laser scanning system developed in this thesis is an extension of the previous refractive systems found in the literature. In this section, a brief summary of those systems are discussed.

5.1.2 Scanning with Monolithic Lenses

Figure 5.1 depicts a refractive beam scanning system in its simplest form. The actual scanning unit consists of two lenses (L_1 and L_2) separated by a distance of $f_1 + f_2$ (sum of focal lengths of the individual lenses). In the undeflected state, a collimated light beam incident on the stationary lens L_1 is focused onto its Fourier plane and then recollimated by the movable lens L_2 ; hence, the scanning unit has no effect on the propagation direction. For scanning to occur L_2 or L_1 should be translated along the direction perpendicular to the optical axis. The relation between the translation and the scan angle can be derived using

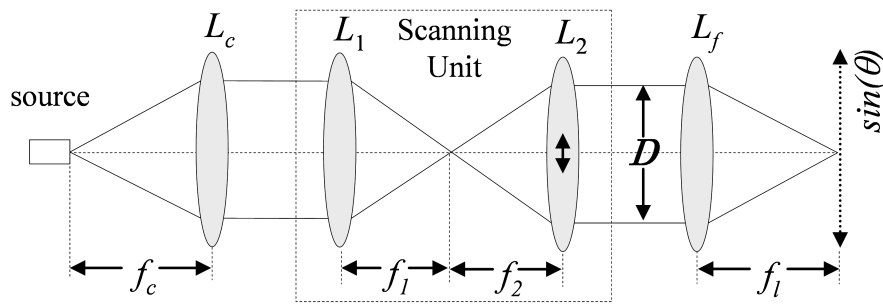


Figure 5.1: Simplest refractive beam scanning configuration with a lens translating perpendicularly with respect to the optical axis. L_c : Collimating Lens; L_f : Focusing Lens; L_1 : Lens 1; L_2 : Lens 2; D : Collimated beam size on L_c . Focal length of each optical component is denoted by the respective subscript.

a matrix-based formalism, which is a slightly modified version of the well known matrix optics method [118]. In this method, the effect of an optical element on a ray is represented

by a 3×3 matrix, such that;

$$\begin{pmatrix} h_{out} \\ \alpha_{out} \\ 1 \end{pmatrix} = \underbrace{\begin{bmatrix} M_{11} & M_{12} & r \\ M_{21} & M_{22} & q \\ 0 & 0 & 1 \end{bmatrix}}_{\mathcal{M}} \begin{pmatrix} h_{in} \\ \alpha_{in} \\ 1 \end{pmatrix} \quad (5.1)$$

where, r and q denote the translation and rotation of the optical element with respect to the optical axis, respectively and h and α parameters account for the rays height from and its angle between the optical axis. Please note that, as an extension of the basic matrix optics, such a representation holds within the paraxial limit ($\sin \theta \approx \theta$). Using this formalism, transfer matrix for the scanning unit with L_2 displacement r_0 in Figure 5.1 is derived below. For the sake of simplicity, all lenses are assumed to be thin and have focal lengths of f .

$$\begin{aligned} \mathcal{M} &= \underbrace{\begin{bmatrix} 1 & 0 & 0 \\ -1/f & 1 & 0 \\ 0 & 0 & 1 \end{bmatrix}}_{L_2} \underbrace{\begin{bmatrix} 1 & 0 & -r_0 \\ -1/f & 0 & 0 \\ 0 & 0 & 1 \end{bmatrix}}_{CoordinateShift} \underbrace{\begin{bmatrix} 1 & 2f & -0 \\ 0 & 1 & 0 \\ 0 & 0 & 1 \end{bmatrix}}_{Propagation} \underbrace{\begin{bmatrix} 1 & 0 & 0 \\ -1/f & 1 & 0 \\ 0 & 0 & 1 \end{bmatrix}}_{L_1} \quad (5.2) \\ &= \begin{bmatrix} -1 & 2f & -r_0 \\ 0 & -1 & r_0/f \\ 0 & 0 & 1 \end{bmatrix} \end{aligned}$$

Applying this transfer matrix on the chief ray reveals the effect of the translating lens:

$$\begin{bmatrix} -1 & 2f & -r_0 \\ 0 & -1 & r_0/f \\ 0 & 0 & 1 \end{bmatrix} \begin{pmatrix} 0 \\ 0 \\ 1 \end{pmatrix} = \begin{pmatrix} -r_0 \\ r_0/f \\ 1 \end{pmatrix} \quad (5.3)$$

Hence, rotation angle of the collimated beam after leaving the system is found to be:

$$\theta = \frac{r_0}{f} \quad (5.4)$$

Within the paraxial regime, the scan angle varies linearly with lens translation, and it follows from 5.3 and the symmetry of the system with respect to the optical axis that the maximum scan angle is limited to

$$\theta_{max} = \frac{D}{2f} \quad (5.5)$$

In order to be able to utilize the full scanning range of the system given in Figure 5.1, L_2 should be translated by half of the incoming beam size, which should be chosen considerably large to reduce the diffraction limited spot size on the image plane. However, this is a significant problem for a high resolution display /imaging engine, since fast translation of a bulky refractive element along a long range is a challenging task. Microlens arrays provide an effective solution to this issue.

Scanning with Two Cascaded Microlens Arrays

An MLA based refractive beam scanner consisting of two MLAs separated by a distance of two focal lengths, as shown in Figure 5.2 [119]. In this system, the incident collimated beam is separated into several bundles, and each bundle is focused onto an intermediate image plane by the lenslets in the 1st MLA. The 2nd MLA recollimates these bundles and reconstructs the incoming beam. Similar to the monolithic lens scanner system, angular displacement of the beam is accomplished by the lateral translation the MLAs relative to each other. However, for this configuration, as it is shown below, the required MLA displacement is significantly smaller (by a fraction of D/d , to be precise) than the monolithic lens scanner, and, therefore, rapid scanning is more feasible. It can be noted that each

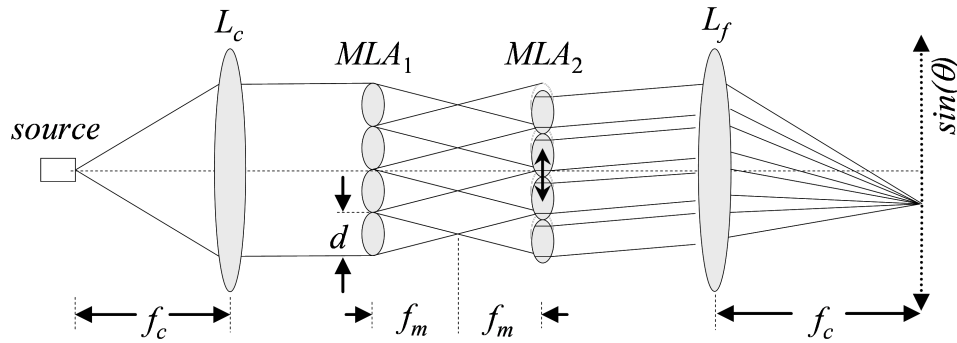


Figure 5.2: Beam scanning with two cascaded MLAs

cascaded lenslet pair in Figure 5.2 behaves as a separate scanning unit (*channel*); hence, the matrix analysis given in 5.3 also holds for this configuration, with the bulk lens parameters replaced with lenslet parameters. This follows that the scan angle is given by:

$$\theta = \frac{r_0}{f_m} \quad (5.6)$$

The advantage of the MLA scanning is evident. The translation required to attain the same beam deflection as the monolithic lens scanner is smaller by a factor of f/f_m . Despite this obvious advantage, however, there are two main drawbacks associated with the two-MLA scanning scheme that significantly limit its use: i) spurious light, ii) discrete addressability of the image plane. These two effects are separately investigated below.

The *spurious light* is due to the overlap of diverging bundles with the neighboring recollimating microlens, and grows with increasing MLA displacement (Figure 5.3). The main negative effects of this problem are the decrease of light efficiency and degradation of the point spread function [118].

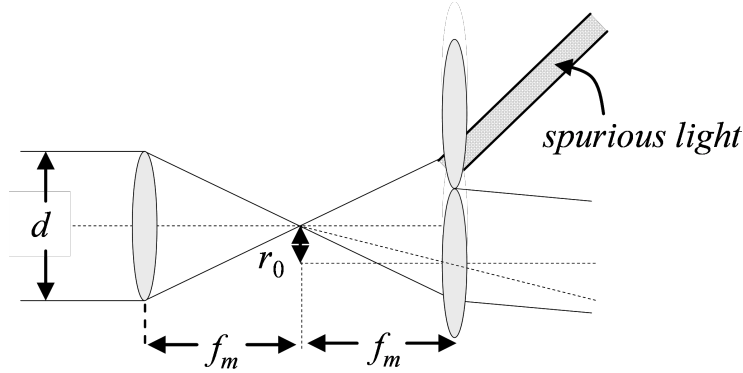


Figure 5.3: Lateral displacement of recollimating MLA results in beam deflection. Generation of spurious light that is due to the overlap of diverging bundles with the neighboring recollimating microlens.

The Field Lens Array

As illustrated in Figure 5.4, an efficient method to eliminate the spurious light problem is to place a third MLA, which is called the field lens array (FMLA) at the focal plane of the focusing 1st MLA [118]. The field lens array and the recollimating MLA can be implemented on the same substrate as a double-sided MLA (DMLA). The effect of the field lens array on the diverging bundle is illustrated in Figure 5.5. Due to its position, the field lens array acts similar to a prism. Without microlens displacement, the field lens array has no effect on the ray bundle; with increasing DMLA displacement, the field lens array bends the ray bundle in the direction of the displacement and makes the chief ray go through the center

of the 2^{nd} surface of the DMLA, eliminating the spurious light. The transfer matrix of the

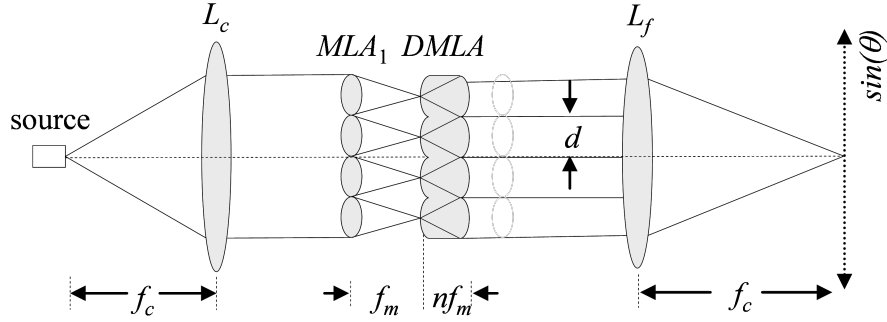


Figure 5.4: Microlens beam steering system with the field lens array. The field lens array (FMLA) is placed at the focal plane of the 1^{st} MLA and moves together with the 2^{nd} MLA. The 2^{nd} MLA the FMLA can be fabricated to rest on two sides of the same substrate to form a *double-sided microlens array* (DMLA).

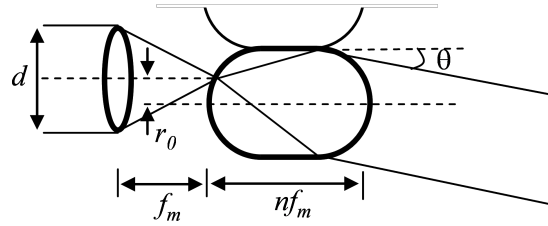


Figure 5.5: Elimination of the spurious light problem by the field lens. The field lens deflects the diverging bundle towards the direction of DMLA displacement and keeps the it within the aperture of the recollimating lens

3-MLA configuration depicted in Figure 5.4 can be computed as follows:

$$\mathcal{M} = \underbrace{\begin{bmatrix} 1 & 0 & 0 \\ -1/f_m & 1 & 0 \\ 0 & 0 & 1 \end{bmatrix}}_{MLA3} \underbrace{\begin{bmatrix} 1 & f_m & -r_0 \\ 0 & 1 & 0 \\ 0 & 0 & 1 \end{bmatrix}}_{Propagation} \underbrace{\begin{bmatrix} 1 & 0 & 0 \\ -1/f_m & 1 & 0 \\ 0 & 0 & 1 \end{bmatrix}}_{MLA2} \times \quad (5.7)$$

$$\underbrace{\begin{bmatrix} 1 & 0 & -0 \\ -1/f_m & 0 & 0 \\ 0 & 0 & 1 \end{bmatrix}}_{CoordinateShift} \underbrace{\begin{bmatrix} 1 & f_m & -0 \\ 0 & 1 & 0 \\ 0 & 0 & 1 \end{bmatrix}}_{Propagation} \underbrace{\begin{bmatrix} 1 & 0 & 0 \\ -1/f_m & 1 & 0 \\ 0 & 0 & 1 \end{bmatrix}}_{MLA1}$$

$$= \begin{bmatrix} -1 & f_m & 0 \\ 0 & -1 & r_0/f_m \\ 0 & 0 & 1 \end{bmatrix}$$

The effect of this system on the chief ray is:

$$\begin{bmatrix} -1 & f_m & 0 \\ 0 & -1 & r_0/f_m \\ 0 & 0 & 1 \end{bmatrix} \begin{pmatrix} 0 \\ 0 \\ 1 \end{pmatrix} = \begin{pmatrix} 0 \\ r_0/f_m \\ 1 \end{pmatrix} \quad (5.8)$$

Equation 5.8 reveals that the field lens array eliminates the spurious light problem: the decentration component of the ray vector after the DMLA is zero, thus there is no spurious light. The amount of beam deflection is not affected by the use of the field lens and the deflection angle is given by r_0/f_m . The discrete addressability problem is more involved than spurious light, and can not be described with the methods of geometrical optics, since it is purely a diffraction phenomenon. The phase profile of the beam after the DMLA is shown in Figure 5.6a. Assuming square-packed MLAs with 100% fill factor and spherical profile and plane-wave illumination, the phase can be described by a multiplication of two terms: a tilted linear phase profile over the entire aperture D , and a linear phase distribution of the entire aperture over a single lens aperture with the periodicity of the lens spacing, which can be expressed as:

$$t(x) = \text{rect}\left(\frac{x}{Nd}\right) \left[\left(\exp\left(\frac{-i2\pi r_0 x}{\lambda f_m}\right) \text{rect}\left(\frac{x}{d}\right) \right) * \sum_{n=-\infty}^{n=\infty} \delta(x - nd) \right] \quad (5.9)$$

where, $*$ denotes convolution, d is the microlens pitch size, N is the number of lenslets in the MLAs and $\text{comb}(x/d)$ is the periodic train of impulses separated by a distance of d . Using Fourier optics principles described in Appendix B, the far-field intensity profile of the diffraction pattern of 5.9 can be computed as:

$$\begin{aligned} I(\theta) &= (Nd)^2 \text{sinc}^2\left(\frac{Nd \sin \theta}{\lambda}\right) * \left[\text{sinc}\left(\frac{d(\sin \theta - \sin \beta)}{\lambda}\right) \sum_{n=-\infty}^{n=\infty} \delta\left(\frac{\sin \theta}{\lambda} - \frac{n}{d}\right) \right]^2 \\ &\approx (Nd)^2 \text{sinc}^2\left(\frac{d(\sin \theta - \sin \beta)}{\lambda}\right) \sum_{n=-\infty}^{n=\infty} \text{sinc}^2\left(Nd\left(\frac{\sin \theta}{\lambda} - \frac{n}{d}\right)\right) \end{aligned} \quad (5.10)$$

where, θ is in units of radians and represent the angle of propagation. Equation 5.11 has two multiplicative angle dependent terms that shape the diffraction profile. The first term I_1 is a *sinc* function whose width depends only on the lenslet pitch size and can shift

on the diffraction plane depending on the displacement DMLA displacement. The second term I_2 is the convolution of a periodic impulse train and a *sinc* function of width λ/Nd . Assuming that the width of this sinc function is very small compared to the period of the impulses (which should be the case for a high resolution scanning engine), the tails of the individual sinc functions can be ignored and I_2 can be approximated as a periodic train of sinc functions that form a grid on the diffraction plane. As the DMLA is translated laterally, I_1 moves over the static grid formed by I_2 . Figure 5.6a illustrates such a diffraction profile for three different DMLA displacement values, and illustrates the discrete addressability problem. Without any decentration ($r_0 = 0$), only the 0^{th} diffraction order can survive the multiplication of I_1 and I_2 , therefore a single spot is observed on the diffraction plane. As the DMLA is decentered, I_2 remains intact, but I_1 begins to shift, so that intensity of 0^{th} diffraction order begins to decrease and $\pm 1^{st}$ diffraction orders becomes visible. At a certain point, the intensities of these order become equal and the spot on the diffraction plane is split into two (Figure 5.6b). Further displacement of the MLA strengthens the $\pm 1^{st}$ diffraction orders and when $r_0 = f_m/(\lambda d)$, the diffraction profile is a single spot located at the 1st order (Figure 5.6c). Due to the grid-like profile of I_2 , intermediate angles between the diffraction orders can not be addressed by sole decentration of the MMLA.

5.1.3 Continuous MLA Scanning

A novel method to overcome the discrete addressability problem and maintain continuous addressability of the image plane is developed. The key idea behind the method is the shifting of the entire diffraction pattern by pre-tilting the collimated beam incident on MMLA. The easiest way to achieve this pre-tilt is slightly moving the lens in front of the MMLA, which is called the Pre-Scan Lens (PSL). Figure 5.7 shows the proposed scanning system with the movable PSL. DMLA can be moved in discrete steps equal to $f_m/(\lambda d)$ to address different diffraction angles once DMLA is settled to address certain position, the PSL can be moved continuously to address points in between the discrete diffraction orders. The coordinated small motions of the PSL and DMLA can perform a full-range/continuous scanning of the space. Hence, continuous high resolution refractive scanning can be accomplished by small translation of two optical components, instead of large displacement of a single lens. In order to analyze PSL's contribution the 3-MLA block can be modeled as a par-

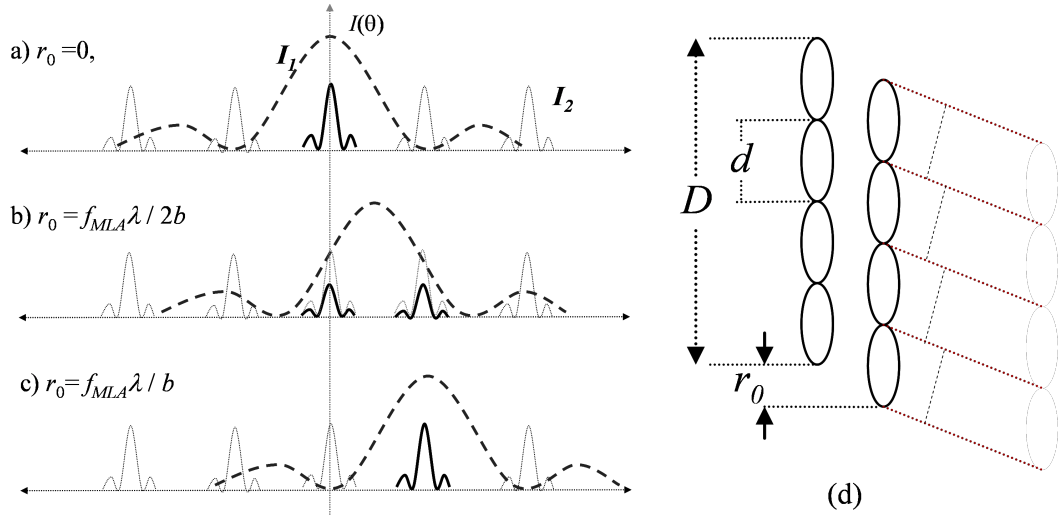


Figure 5.6: (a) The phase profile of the tilted beam after the 2nd MLA. Due to the periodicity of the lenslets, the beam has a periodic phase profile, which produces a discrete diffraction pattern on the image plane (b) Discrete addressability problem. Two factors of the diffracted intensity profile are shown with the dashed lines. Actual spot profile is depicted by the solid lines

allel channelized processor, where each channel consists of three identical lenses with one focal distance separation and analyzed using Fourier optics. The effect of such a channel on propagation direction can also be shown using the matrix approach. By applying the transfer matrix of the 3MLA system of Figure 5.4 with r_0 DMLA decentration onto a ray with incidence angle $-\alpha$:

$$\begin{bmatrix} -1 & f_m & 0 \\ 0 & -1 & r_0/f_m \\ 0 & 0 & 1 \end{bmatrix} \begin{pmatrix} 0 \\ -\alpha \\ 1 \end{pmatrix} = \begin{pmatrix} f\alpha \\ r_0/f_m + \alpha \\ 1 \end{pmatrix} \quad (5.11)$$

The resultant ray vector has a total tilt angle which is a difference of the MMLA contribution and the pre-tilt. Equation 5.11 also reveals that pre-tilt creates some spurious light that arises due to the lateral shift of the beam by $f_m\alpha$. However, as it will soon be shown, the maximum required pre-tilt angle is limited to half the diffraction angle (λ/d), and the maximum lateral beam shift will be

$$\text{maximum beam shift} = f_m\alpha \frac{\lambda}{d} = \frac{\lambda}{f_{\#,mla}}. \quad (5.12)$$

Hence, the amount of spurious light is very small. Figure 5.8a illustrates the propagation

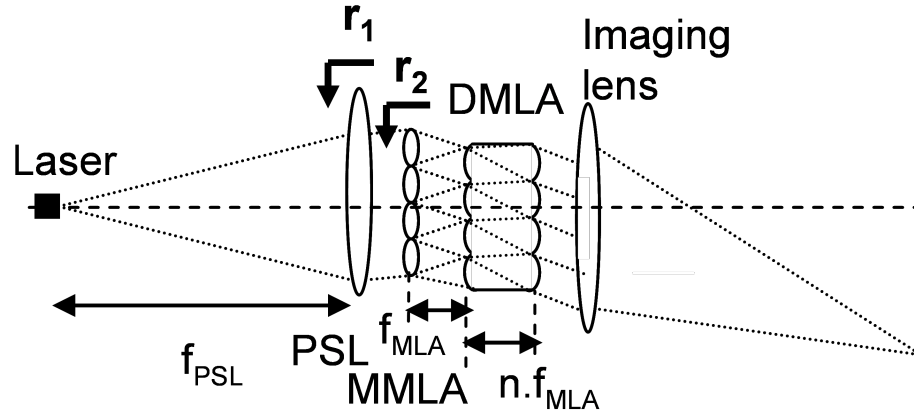


Figure 5.7: Microlens beam steering system with the field lens array. The field lens array is placed at the focal plane of the 1st MLA and moves together with the 2nd MLA. The field lens array and the recollimating MLA can be implemented on a single substrate (DMLA)

of a plane-wave through a 3 lens system when L_2 and L_3 coordinate axis are shifted. L_1 forms a focused spot, L_2 and L_3 combination forms an optical Fourier transform module and converts the displaced spherical wavefront into a plane wave with tilt angle β . Figure 5.8b illustrates that if a tilted plane-wave of angle $(-\alpha)$ were incident on 3- identical lenses on the same optical axis, the propagation vector becomes parallel to the optical axis after L_2 , therefore, the exiting wavefront would be another plane wave with angle $(+\alpha)$, which is effectively rotated by 2α . Hence, if the pre-tilt is accomplished by decentration of the PSF

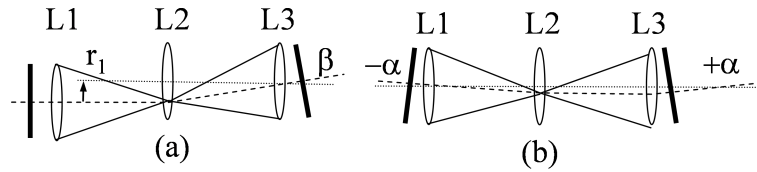


Figure 5.8: Propagation of collimated beam through a 3-microlens channel when (a) L_1 is displaced by r_1 and (b) the incident beam is tilted with angle $-\alpha$.

by r_1 , the total beam scan angle can be written as the sum of two independent contributions α and β , such that:

$$\alpha = -r_1/f_{PSL} ; \beta = r_2/f_{MLA} \quad (5.13)$$

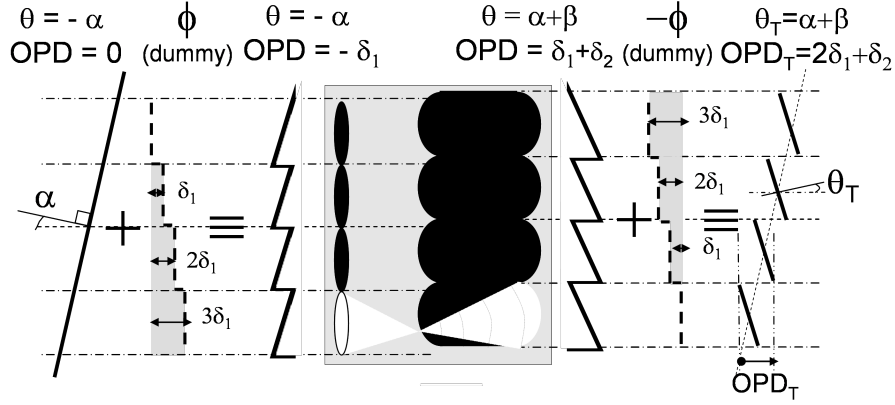


Figure 5.9: Physical optics view of the 3-MLA block system after the PSL. Scan angle and OPD between beam segments is marked after each element. (Dummy phase shifters have no net effect in the system)

It is now shown that, from a geometrical optics point of view, PSL and MMLA contributions to the total scan angle are additive. However, the effect of PSL does not alter the periodicity of the phase profile after the DMLA; hence the diffraction effects are still present. A complete understanding of the continuous scanning requires an in-depth Fourier analysis of the system, which can be found in [120]. Here, a more qualitative approach is pursued. Figure 5.9 illustrates the physical optics view and the operation of the MLA scanning system. For simplicity, the incident planar wavefronts on each microlens channel is made identical by adding a different linear phase at the entrance to each channel, which are then subtracted at the exit of each channel; thus the net effect of the phase shifters are zero. The dummy phase function forms a step function ϕ with step heights of $\delta_1 = \alpha d$, which correspond to the OPD between neighboring channels. Thus, the contribution of the PSL to θ and OPD are as 2α and $2\delta_1$, respectively. In the same manner, assuming a plane parallel wave incident on the 3-MLA block, the MMLA contribution to beam rotation and OPD are β and $\delta_2 = \beta d$, respectively. The total beam scan angle θ_t and the phase difference between wavefront segments at the exit of the system OPD_t are expressed as the sum of independent contributions of the PSL and the MMLA:

$$\begin{aligned}\theta_t &= \alpha + \beta \\ OPD_t &= 2\delta_1 + \delta_2 = (2\alpha + \beta)d\end{aligned}\tag{5.14}$$

It intuitively follows that, to eliminate the periodicity of the phase profile at the exit of the DMLA, constructive interference between wavefront segments is required. Therefore, OPD_t should satisfy the following *phase condition*:

$$OPD_t = n\lambda, n = 0, \pm 1, \pm 2, \dots \quad (5.15)$$

Combining equations 5.13, 5.15 and 5.15, it can be shown that to address an arbitrary angle on the image plane, the PSL and DMLA decentration variable r_1 and r_2 should be

$$r_1 = (\theta_t - n\lambda/d) f_{PSL} ; r_2 = (\theta_t + r_1/f_{PSL}) . \quad (5.16)$$

Figure 5.10 is the simulated PSF of the system in normalized angular coordinate system assuming plane-wave illumination, square-packed 100% fill-factor spherical MLAs, and thin lens approximation. A combination of angular and far field beam propagation methods were employed [120]. In Figures 5.10a and 5.10b, the PSL is at rest, and only the integer multiples of the diffraction angle can be properly addressed. If the phase condition is not met, as in Figure 5.10b, the resultant PSF is split between two diffraction orders. However, in Figure 5.10d, the phase condition is met, and constructive PSF at $\theta = 0.4\lambda/d$ is obtained.

Table 5.1: System features and corresponding number of resolvable spots with FWHM definition. (Plane monochromatic illumination at $\lambda = 632nm$, $f_{\#,psl} = 3$)

$D(mm)$	$f_{\#,mla}$	$d(mm)$	$r_{1,max}(\mu m)$	$\theta_{max}(mrad)$	N_R
2	4	0.1	± 19	± 125	768
2	4	0.2	± 9.5	± 125	768
3	4	0.1	± 28.5	± 125	1152
3	4	0.2	± 14	± 125	1152
3	3	0.1	± 28.5	± 167	1536
3	2	0.1	± 28.5	± 250	2304

5.1.4 Optical Performance Analysis

The optical performance aspects of the continuous MLA scanner method, such as resolution, aberrations, and MTF are covered next. Bulk of the material of this subsection is adapted

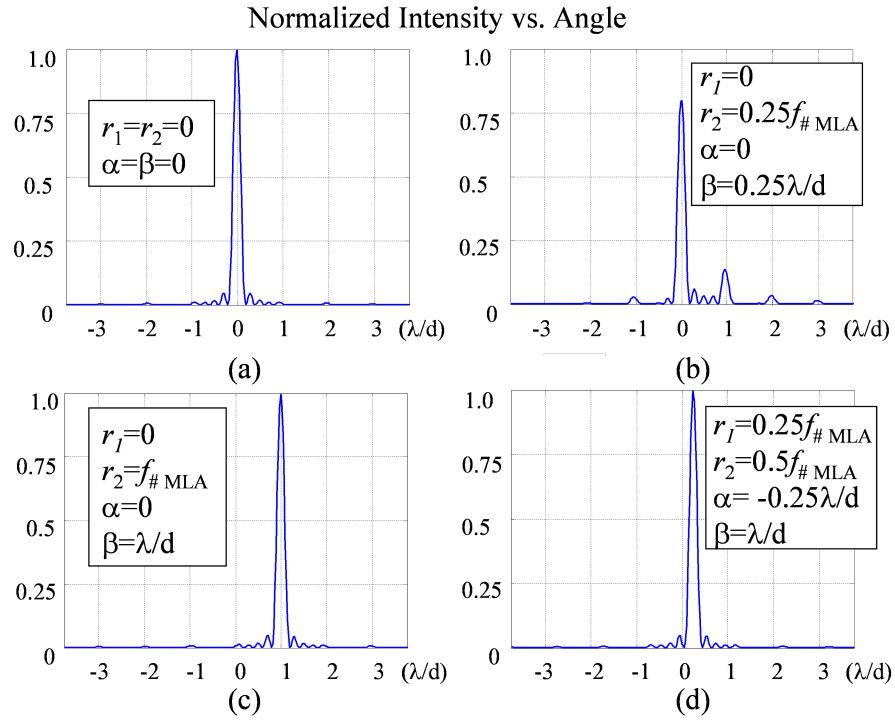


Figure 5.10: Simulated PSF for different amounts of the PSL and the MMLA displacements (r_1 and r_2) and the resultant beam tilt angles. (a), (c), and (d) illustrate constructive beam interference (phase condition is met) and (b) illustrate destructive interference (phase condition is not met)

from Ata Akatay's master's thesis. The reader is directed to that reference for a more comprehensive discussion optical system performance and optimization [120].

Resolution

The fundamental figure of merit for a display or imaging engine is the resolution of the displayed/acquired image, which can simply be defined as the ratio of total image size to the smallest resolvable spot size. Resolution of the optical system can be determined by

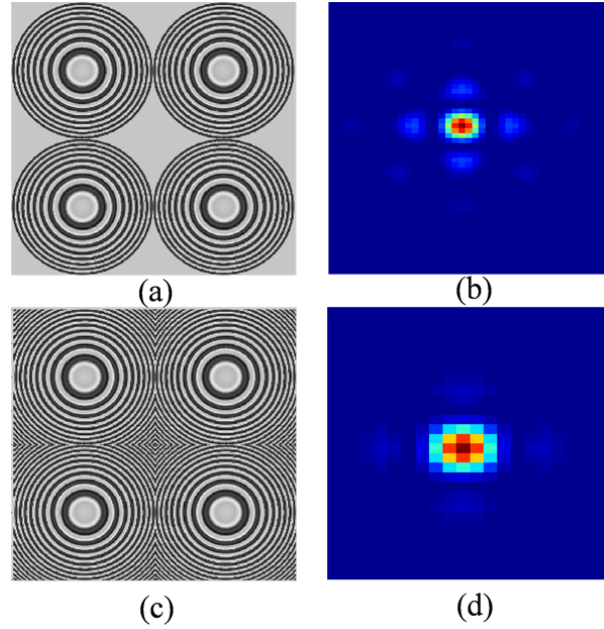


Figure 5.11: Effects of MLA fill factor on the PSF of the scanning system in the undeflected state. (a) Phase plot of low fill-factor MLA, (b) Corresponding PSF, (c) Phase plot of 100% fill-factor MLA, (d) Corresponding PSF.

the total scan angle divided by the minimum angular spot size which is defined by the I_2 and approximately equal to λ/D . Note that the spot size is independent of the aberrations and the fill-factor. Appearing aberrations and non-uniform intensity distribution cause a reshaping of the envelope function (I_1) and modifies the distribution of the optical energy between the diffraction orders. The field distribution pattern at a single diffraction order is simply the scaled far-field pattern of a full aperture beam ($\text{sinc}(D \sin \theta / \lambda)$), and always size-wise diffraction limited and magnitude-wise dependent on the aberration performance of the system. When the PSF is composed of a number of separated diffraction orders,

resolution can not be simply determined by using the minimum angular spot size of the field in a single diffraction peak.

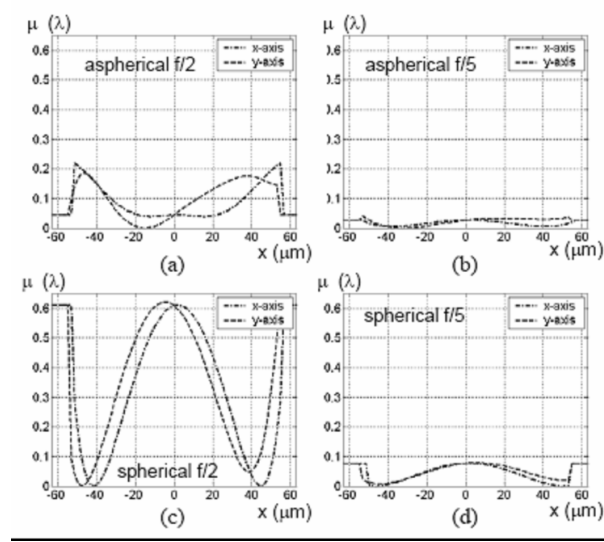


Figure 5.12: Wavefront aberrations plotted in x and y directions for the systems utilizing 100 μm pitched (a) aspherical f/2 (b) aspherical f/5 (c) spherical f/2 (d) spherical f/5 MLAs.

Neglecting the aberrations and assuming paraxial approximation is valid (i.e., small angles), the number-of resolvable spots (NR) that can be obtained by this scanning system can be expressed as [122]:

$$N_R = \frac{2\theta_{max}D}{K\lambda} = \frac{D}{Kf_{\#mla}\lambda} \quad (5.17)$$

where θ_{max} is the maximum optical scan angle and K is a beam constant incorporating the beam profile and Gaussian beam clipping effects and the assumptions related to the system design such as amount of overlap between adjacent pixels. Table 5.1 shows the system resolution as a function of system parameters assuming diffraction limited performance. Maximum resolution of 2300 pixels in each axis can be achieved with 3 mm clear aperture and $f/2$ microlenses. A maximum scan angle of ± 0.25 rad is achieved by moving the PSL and the MMLA by $\pm 28 \mu\text{m}$ and $\pm 28 \mu\text{m}$, respectively. To achieve the same scan angle and resolution with 3mm clear aperture, an $f/2$ monolithic lens scanner system would need to move by $\pm 1500 \mu\text{m}$ and a scanning mirror would have a total tip deflection of $\pm 188 \mu\text{m}$.

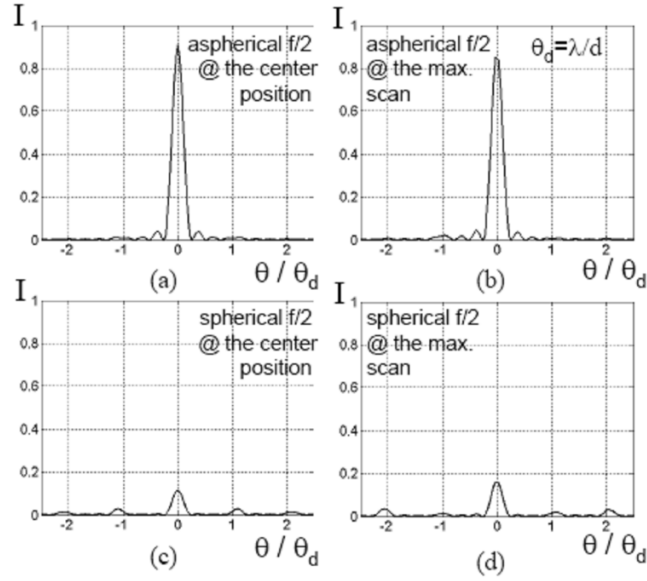


Figure 5.13: PSF cross-sections of the systems for (a) aspherical f/2 at the center position (b) aspherical f/2 at the max. scan angle position (c) spherical f/2 at the center position (d) spherical f/2 at the center position [120]

PSF and Aberration

Fill factor of the MLAs is critically important, for the image quality, since low fill factor MLAs are prone to spurious light problems. The spurious light passing through uncovered blank areas causes additional diffraction effects and spoils the image. In rectangular packing the fill factor of the MLAs reduces to 85% for circular lenses and 100% fill-factor is achievable for square lenses. Figure 5.11 compares the image PSF of the system for the two cases. The fundamental difference between the PSFs shown in Figures 5.11b, and 5.11d is the strength of the side lobes, which contributes inter-pixel cross-talk and degrade the image contrast. For the circular lens MLAs, the side lobe height is 34% of the peak, while for the square lens MLAs, side lobe height reduces to 8%.

Spherical aberrations of the lenslet themselves may manifest themselves as a more significant performance limiting factor. An imaging system suffers more from spherical aberrations with faster lenses. The spherical aberrations introduced by MLAs cause the beam after the MLAs deviate from a collimated beam and so focal spot size increases and ultimately the resolution of the system decreases. Figures 5.12 and 5.11 depicts the simulation results

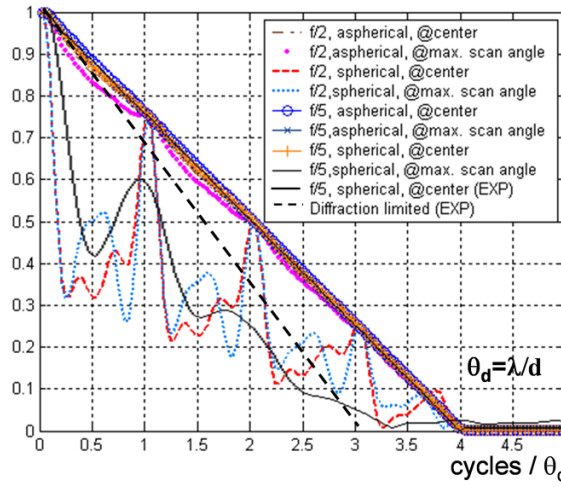


Figure 5.14: MTF plots for the systems of various configurations

comparing scanning systems with the spheric lens and aspheric lens arrays, in terms of wavefront aberrations and PSF performance, respectively.

The dramatic improvement of using aspheric lens arrays is obvious, such MLAs should be employed for the scanning system to utilize the wide scanning range to the full extent.

MTF

A more encompassing performance criterion than PSF for assessing resolving power of an imaging system is the modulation transfer function (MTF), which describes the ability of a system to transfer object contrast to the image as a function of spatial frequency, i.e. as a function of system resolution [81]. The MTF comparison for scanning systems with several different lens parameters shown in Figure 5.14 further solidifies the result that maintaining diffraction limited performance over the entire image plane is only possible with aspheric lenses.

5.1.5 Proof of Principle Experiments

A set of proof-of-principle experiments were performed, for which a full-system as in Figure 5.7 was built with both the PSL and the MMLA mounted on moving stages (Figure 5.15a is a photograph of the experimental system). As the PSL, a bi-convex lens with 4 mm diameter and 8 mm focal length ($f/2$) is used. The polymer (SU8) microlens arrays provided by

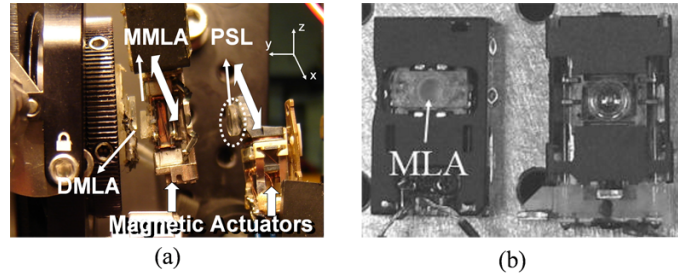


Figure 5.15: The MLS scanner demonstrator (a) Setup photograph (b) Close up view of the magnetic CD drive scanner heads bearing the MMLA and the PSL.

Microvision, Inc. have $200\text{ }\mu\text{m}$ pitch size, 1 mm focal length. The arrays were fabricated by Microvision, Inc with a commercial process. The DMLA was manually constructed from

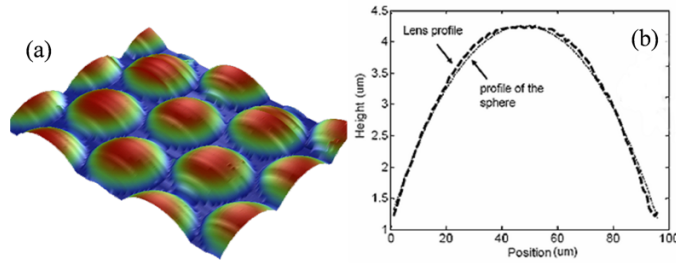


Figure 5.16: 3D view of the MLAs used for proof-of-principle experiments, measured with a stylus profilometer (a), and the comparison of the measured profile with an ideal spherical lens profile (b). The lenslets have $200\mu\text{m}$ pitch size, 1 mm focal length, and are implemented on a 1.05 mm glass substrate. Fabrication of the arrays was done by Microvision, Inc, with a commercial micro-molding process.

two single sided MLAs with a procedure constituted by a micro-alignment process followed by permanent bonding with UV curing polymer. Accurate alignment in all six degrees freedom is a major issue when working with microlens arrays. Due to the small pitch size of the lenses, slight misalignments in the system may propagate to the image plane and significantly degrade the optical performance. Far-field alignment is a powerful technique that greatly facilitates microlens alignment, and is used extensively while constructing the DMLA and aligning it with the MLA. A dedicated instrument was built by the author for this purpose; a detailed description of this setup is presented in Appendix D. A $10\times$ microscope objective is placed after the DMLA to focus the steered light onto a CCD

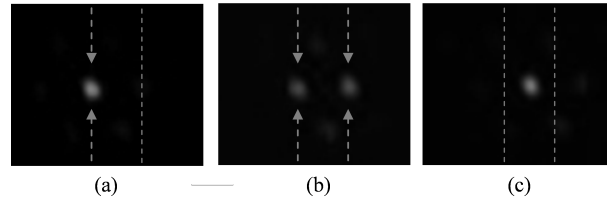


Figure 5.17: Experimental results for the MMLA and the PSL motion. (a) All lenses on-axis, (b) Only the MMLA is moved to steer beam by half diffraction angle (phase condition not met); (c) Both the PSL and the MMLA is moved to steer the beam by half diffraction angle (phase condition met). Experiments are performed with a 532nm green diode laser.



Figure 5.18: Experimental results of the line scans. (a) The MMLA motion only produces discrete addressing; (b) The PSL and the MMLA move synchronously and phase condition is met at all times and continuous addressing is achieved (imperfections in the line is due to slight misalignment of the setup). (c) PSFs at five consecutive diffraction orders

camera. A five-axis micro-stage carrying the MLA is used for fine tuning of the system. The motion of the PSL and the MMLA is controlled by a PC with data acquisition interface, and control software running over MATLAB. The movable elements of the system, the PSL and the MLA, are placed on magnetic actuators extracted from CD-ROM drivers (Figure 5.15b). These actuators employ two permanent magnets placed parallel to each other to create a constant magnetic field in the horizontal direction. The lens holder is positioned in between and has two perpendicularly oriented copper coils, which are exposed to magnetic force when current passes through them. Due to the right hand rule, a current passing through the coil that lies along y direction creates a force along z direction, which is used for dynamic focusing of the lens. Perpendicularly (y) oriented force is induced by the other coil and used to accurately move the lens in 1 m long steps. Despite their excessive size when compared to MEMS devices, CD actuators are capable of high resolution and repeatable actuation, making them very suitable for demonstration purposes. Figure 5.17 shows the way PSF is modified with PSL and MMLA motion, and how the phase condition solves

the discrete addressability problem. As it was illustrated in Figures 5.10b and 5.10d, the scan angles that are not addressable by the MMLA motion alone can be addressed with the combined motion of the MMLA and the PSL. Experimental results show that the PSL motion moves both the entire diffraction grid and the centroid of the PSF by the same amount but in opposite directions, as derived before. The MMLA motion, however, moves only the centroid of the intensity pattern while the diffraction grid stays stationary.

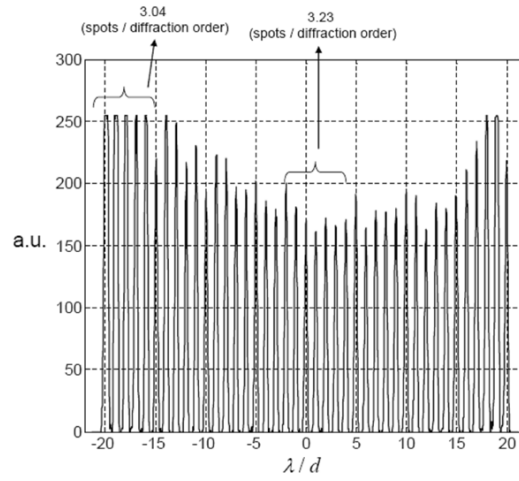


Figure 5.19: PSFs at different scan positions, illustrating resolution of the system. Different magnitudes of the peaks are due to sinusoidal variation of the scanning speed across the scan line

Figure 5.18a demonstrates 41 discrete addressable spots achieved by moving the MMLA, Figure 5.18b demonstrates a scan line with twice as much spots obtained by combined synchronous actuation of the PSL and the MMLA, and finally, Figure 5.18c shows a continuous scan line.

Figure 5.19 plots the diffraction orders in the 2/3 part of the scan line, limited by the aperture size of the 5-axis positioning stage used in the setup. The PSF at the off-axis scan positions broadens about 7% that is mainly due to comatic aberration. As illustrated on the figure and considering the FWHM spot size, at least three spots fit between two diffraction peaks. Hence the number of resolvable spots is in excess of 120 as shown in the Figure. Hence the unclipped full scan line is expected to have 180 resolvable spots in a line. Note that, the theoretical resolution prediction (NR) of the experimental system is 189; hence, the performance is almost diffraction limited. Despite the use of slow and non-optimum

MLAs, and crude scanning devices, the system demonstrated a better performance than the endoscope system from Olympus and several similar systems reported in the literature [36], which have resolution limited to less than 100 spots.

5.2 Specifications for a Micro-Opto-Mechanical Platform

The previous chapter established that the proposed MLA based laser scanner system requires translating microlens arrays at high speed and small amplitude. Micro-electromechanical system technology offers the means to accomplish this task. In this section, optical and mechanical specifications for a micro-mechanical/micro-optical platform dedicated to the developed scanning method are established.

There are two fundamental approaches for integration of a micro-mechanical moving platform with micro-optics, such as depicted in Figure 5.20. The straightforward approach is to fabricate mechanical optia components separately, and then integrate them using manual or automated microassembly. This method has been useful for various applications where alignment tolerances are not very stringent [123], [47]. Petreman, et. al. has developed a 2D MEMS platform with post-process micro-optics integration for a fiber coupler [124]. The system featured close loop control to optimize the coupling lens position for maximum coupling efficiency. The static position of the lens is not critical in this case, since any misalignment during integration can be corrected. The author has collaborated with the developers within the European network NEMO, to adapt and optimize this MEMS platform for the MLA scanner system. Although the effort yielded a 2D MEMS platform with manually integrated microlens arrays (fabricated by Microvision), the alignment tolerance of manual integration was far from satisfying the specifications of the MLA scanner. Specifically, the linear translation axis of the MEMS platform did not coincide with the orientation of the lens arrays. The detail of this work were presented in a conference article by the author. The other approach to integrate micro-mechanical and micro-optical components is to fabricate them on a common platform. Due to the precision alignment capability of the microfabrication process, misalignment of mechanics and optics is generally not an issue. Moreover, integrated fabrication eliminates the cumbersome micro-assembly step; however, most such processes are complex, and require dedicated process development. Reflow lens are the most common micro-optical components fabricated on micro-mechanical, due to

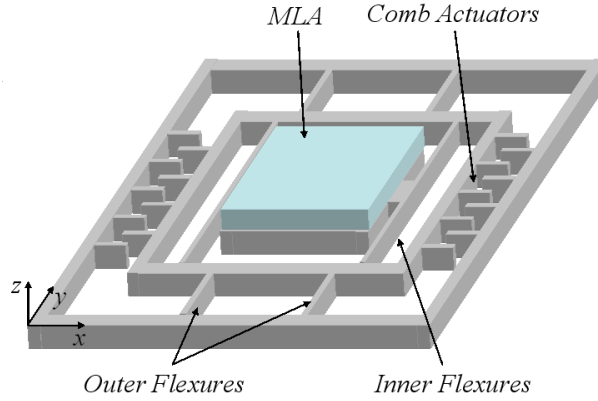


Figure 5.20: An illustration of the micro-optical scanner. The micro-optical component should be fabricated on top of a MEMS platform

their wide use, and relative ease of fabrication. Photoresist, SU8, or any other microfabrication compatible polymer are the common lens materials; however, other materials, such as glass can also be used [123]. Figure 5.7 of the last section showed that MLA fill-factor should be very close to %100 for good optical performance; but, the reflow lens technique is not compatible with this requirement. A more novel fabrication technique more convenient to fabricate 100% lens arrays was developed by Hedsten et. al. using hot embossing of a polymer with high chemical resistivity (Cytop) to form diffractive lenses on SOI wafers [126]. Unlike conventional methods, lens formation precedes standard SOI-MEMS process.

The micro-optical scanner architecture chosen for this thesis project is similar to Hedsten's device; however, the fabrication method for the microlens arrays is significantly different than the diffractive microlens that system employs. The author had the opportunity to collaborate with Hedsten's group in Chalmers University, Sweden through the *MC2 Access* program to develop a fabrication process for the designed micro-optical scanner.

The mechanical performance specifications for the micro-optical scanner is mainly determined by the microlens array parameters, such as the maximum required travel range, which equals to the half the microlens pitch size. Table 5.2 lists some major parameters of various MLA designs, and the 6th MLA type was the fabrication choice. The selection of the lens parameters was guided by the fabrication limitations associated with the maximum spin coating thickness of the chosen polymer Cytop. This selection of lens properties impose the mechanical specifications given in Table 5.3. A strict specification has not been

Table 5.2: Geometrical parameters for square packed MLAs fabricated from Cytop ($n = 1.34$). All dimensions are in microns, except the dimensionless $f_{\#}$

	1	2	3	4	5	6
MLA side pitch	150	150	100	100	75	75
MLA diagonal pitch	212	212	141	141	106	106
$f_{\#,mla}$	3	4	3	4	3	4
Focal Length	450	600	300	400	225	300
Radius of curvature	153	204	102	136	76.5	102
Max lens height	42.73	29.74	28.49	19.83	21.37	14.87

set on the scan frequency of the micro-optical scanner, but maximization of scanning speed without compromising the scan range. The devices are designed to yield required deflection at 200 V_{pp} sinusoidal excitation.

Table 5.3: Specifications for the micro-optical scanner for $f_{psl} = 3$ mm, $f_{\#,psl} = 3$, $d = 75$ μm , $f_m = 300$ μm , $\lambda = 635$ nm

Diffraction Angle	Max PSL Displacement	Max MLA Displacement
8.46 mrad	± 25.4 μm	± 37.5 μm

5.3 High Fill-Factor Molded Microlens Arrays

Hedsten et. al. have recently shown how diffractive optical elements can be integrated onto silicon microstructures using hot embossing replication to form the optical element [126]. The embossing tool was a conventional press, which is not a standard clean room equipment, nor does it allow for accurate alignment to an existing structure already formed on the wafer. In this work, a hot embossing process is developed using a wafer bonder as the press tool. This novel method eliminates the limitations on the molding process associated with the conventional hot embossing tools. An alternative method could be injection molding; however, this tool can not be used for molding structures higher than a few microns. The details of the MLA process development are published as a conference paper [125]. Hot

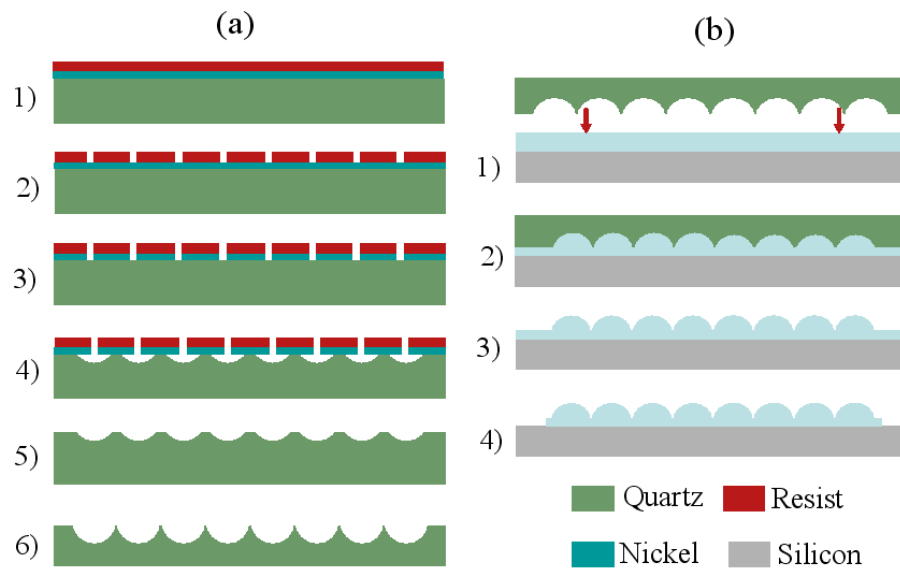


Figure 5.21: (a) Fabrication sequence for the quartz mold 1) Molds will be fabricated on 1.5mm thick quartz wafers. As the first step, the wafer is sputtered with Ni and then spin-coated with photoresist. 2) Masking and developing of the resist layer to define etch holes 3) Etching of Ni layer to open etch holes 4) Isotropic etching of quartz wafer 5) Removal of resist and Ni layers. 6) Further isotropic wet etching of quartz to get 100% fill factor microlens arrays. (b) MLA Fabrication sequence for MLAs Illustration of the hot embossing process. 1) The replication tool; a glass mould with refractive lens arrays. 2) Silicon wafer with a 20 μm thick spun polymer layer on top. 3) The wafer with the spun polymer is brought in contact with the glass mould under pressure at an elevated temperature. 4) Silicon wafer with the replicated polymer lens arrays.

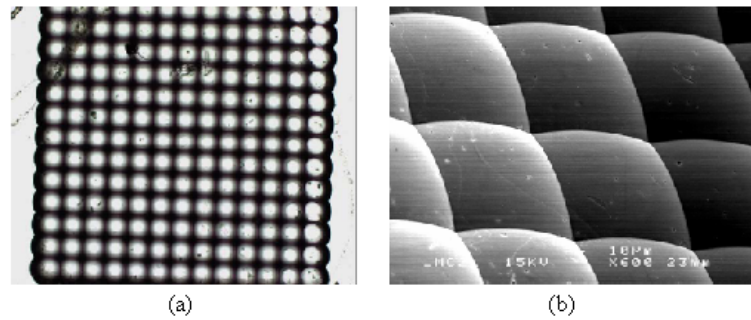


Figure 5.22: (a) Top-view microscope photograph of a replicated refractive lens array, 1×1 mm. The individual lenses are $75 \times 75 \mu\text{m}$. (b) A SEM picture of a part of a replicated lens array, taken at an angle of 60 degrees. To facilitate the SEM measurement the polymer lens array was first coated with a thin aluminum layer.

embossing requires a mold with the negative of the structures to be molded engraved on its surface. The mold used for this work was fabricated in a commercial process by Microvision Inc., USA on a quartz substrate, into which lens engravings were isotropically etched. A cross-sectional drawing of the mould, and basic fabrication steps are shown in Figure 5.21a. The mold fabrication process included the depositing of a release agent on the surface as a final step, to facilitate the separation of mould and replica after the hot embossing step. The lens array structures were transferred to the polymer layer by hot embossing replication. The machine used for this processing step was a substrate bonder, Karl Suss SB6, i.e. a machine usually used for fusion and anodic wafer bonding. The bond chamber of the SB6 supports vacuum ambient. This feature was not used for the hot embossing of the lens arrays, the chamber was kept at atmospheric pressure. The glass mould with the lens arrays was placed on top of the polymer coated silicon wafer, which was then placed in the substrate bonder. To emboss the lens structures on the glass mould into the fluorocarbon polymer on the silicon wafer, the substrate bonder was set to apply a compressive pressure of 7200 mbar at a temperature of 160C for 30 minutes. The temperature was then ramped down and the pressure was released. The wafer-mould package was then taken out of the bonder and the wafer was separated from the mould. 5.22a shows a topview microscope photograph of a lens array and 5.22b is a SEM picture of the same array. Measurements were carried out on the replicated lens array structure using a Tencor P15 surface profiler. A one trace scan of lenses near the centre of the array is shown in Figure 5.23b, and a dense

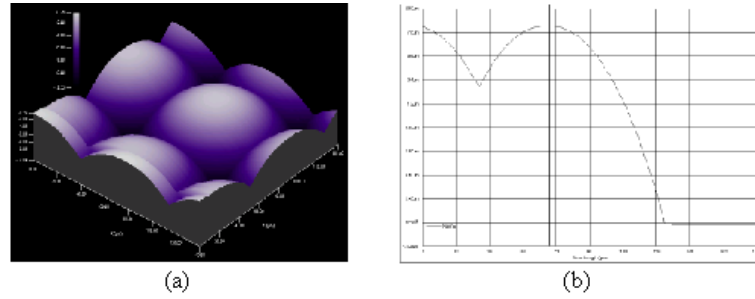


Figure 5.23: (a) A three dimensional surface profile scan of part of a replicated lens array. (b) A surface profile scan of replicated fluorocarbon lenses at the edge of the lens array. The x-grid is $18 \mu\text{m}$ and the y-grid is $2.2 \mu\text{m}$.

three dimensional scan was also carried out and can be seen in Figure 5.22b. The profiler needle has a tip radius of $2 \mu\text{m}$ and is not expected to reach down in between lenslets. The scans show clearly that high enough pressure could be applied in the substrate bonder to replicate the lens array structures successfully. From measurement data in figure 5.22b, a radius of curvature of $130 \mu\text{m}$ can be calculated. This is a larger radius than expected, and is an effect of the profiler needle not reaching the deepest features, i.e. the step height is larger than measured and the real radius of curvature is smaller.

In summary, the feasibility of using a wafer bonder for hot embossed replication on silicon wafers is demonstrated. Experimentally refractive lens arrays were replicated into an amorphous fluorocarbon polymer spun onto a silicon wafer. Surface profile scans of the lens arrays show that the replicated structures have a smooth convex lens shape and that the lens arrays can be transferred with an intact surface profile. Unlike conventional presses, substrate bonders are readily available in most well equipped clean rooms. Further improvement of this fabrication process is also expected, as equipment used for accurate aligned bonding of structured wafers could also be used to align a transparent mould to a structured silicon wafer.

5.4 Comb Actuated Micro-Optical Scanner

Micromechanical translation stages for high precision positioning have proven to be a useful tool for several applications and drew considerable attention from research and industry alike. Such platforms bearing refractive micro-lenses have been developed for efficient fiber-

to-fiber, or laser-to-fiber coupling [124] [126] [39], hard disk optical pick-up heads [127] [128], or confocal microscopy [123] [129]. Scanning probe based data storage systems utilizing MEMS translation stages have also been proposed [130]. A novel application of MEMS translation stages was proposed by our group in a recent publications related to a novel microlens array based laser scanning system [121]. MEMS translation stage devices facilitate the development of precision scanners in the micrometer-to-millimeter range without the need of complicated and bulky control systems. The batch fabrication and inherent integration capabilities of micro-fabrication methods also significantly reduce the system costs, elevating the potential of MEMS stages for the aforementioned applications. In this work, we present such a device with a novel actuation. The device utilizes two sets of electrostatic comb actuators to indirectly actuate the device, eliminating and/or overcoming some major limitations of such actuation scheme.

As established in Chapter 3, the performance of gap-closing electrostatic actuators is limited by the fundamental trade-off between the operation voltage and the gap between the capacitor plates. For a parallel plate actuator, pull-in occurs when the displacement reaches one third of the initial gap, and increasing the initial gap to extend available actuation range leads to a quadratic increase in the actuation voltage. Interdigitated comb drive actuators, as shown in Chapter 3, do not suffer from this fundamental limitation, and can attain large displacements at a reasonable drive voltage. The geometry of comb actuators is such that the width of the comb fingers is usually small in comparison to their lengths, and the gap between the fixed and moving fingers remains constant during motion. Hence, the available stroke for comb actuation can significantly exceed that of gap-closing actuators. Manzardo has demonstrated more than 250 μm displacement with a in-plane comb actuated lamellar grating interferometer [11], and Schenk developed an out-of-plane comb actuated torsional microscanner with more than 60 degrees optical scan angle [131]. Both devices are operated at relatively low voltages. Despite the significant enhancement of motion range compared to the gap closing actuators, comb actuators may still suffer from lateral pull-in, when the movable and fixed fingers snap together at a certain voltage as a result of finite rotational stiffness of the flexible springs suspending the moving comb finger set. There have been several efforts to extend the motion range of comb actuators before lateral pull-in occurs. Majority of those methods concentrated on optimizing the spring designs to maximize the

stiffness along the instability axis [132] [133]. Proper spring design is the key to improve the pull-in performance of the comb actuators, yet still, there have been several other approaches to further extend the actuator stroke before pull-in occurs [134].

In this section, an electrostatic comb actuated 1D MEMS translational platform with mechanical stroke multiplication via mechanical coupling. The device consists of two cascaded frames, where only the outer frame bears comb actuators, and the inner frame is actuated by means of resonant coupling of the outer frame motion through the coupling springs connecting the frames. A similar principle has been utilized for torsional scanners to enhance the scan angle at high frequency. Yalcinkaya et. al. has developed a 2D electromagnetically actuated microscanner of whose fast scan axis is resonantly excited by the mechanical coupling [27]. The coupling-factor of this device reaches 60, allowing low-power operation. Yoda has presented a 1D microscanner also with two cascaded frames and electrostatic comb actuation [31]. To our knowledge, this type of actuation is used for in-plane actuation for the first time. The most important advantages of using mechanical coupling for in-plane comb actuation is the capability of producing large motions using relatively short comb fingers, reduced the risk of lateral pull-in, and reduced air damping at high frequency oscillations.

5.4.1 Device Properties

A 3D model of the platform and a close-up view of the comb actuators are shown in 5.24. Even though the device consists of two cascaded frames it is capable of uniaxial motion only. The two-frame structure is exploited for the mechanical stroke multiplication. The 75 μm wide inner frame is the main functional unit designed to carry refractive optical components, and thus, employs a 1.5 mm x 1.5 mm optical window in the middle to allow light penetration. Via four straight, 775 μm long and 18 μm wide flexures extended along the y-axis, the inner frame is connected to the outer frame. To fix the entire structure, another suspension system consisting of four straight flexures is used. The length and the width of these flexures are 700 μm and 13 μm , respectively. The overall device size is 3.8 mm x 4 mm x 20 μm . Two sets of in-plane comb actuators are placed on both edges of the outer frame parallel to the y-axis. In each comb set the combs are divided into three levels to increase the total number of combs, while only marginally increasing the area. The

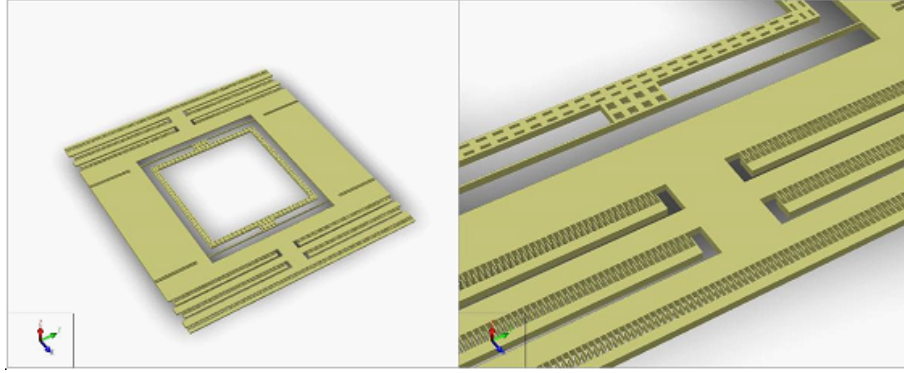


Figure 5.24: Micro-optical scanner geometry without the integrated microlens array. The device consists of two cascaded frames, each suspended by four flexures, all extending along the y-axis. Only the outer frame bears comb actuators, optimized for in-plane translational actuation.

individual combs are $2\ \mu\text{m}$ wide and have a pitch of $12\ \mu\text{m}$.

The dimensions of the device were optimized based on the optical and mechanical specifications, as well as the fabrication limitations. Thickness of the device was set as $20\ \mu\text{m}$ to facilitate fabrication, despite certain advantages of thicker depth, especially in terms of mode separation and device bow. The comb fingers, which determine the minimum feature size of the entire structure, was set to have a width of $2\ \mu\text{m}$ to get a maximum aspect ratio of 10 with $20\ \mu\text{m}$ thick SOI device layer. The limit on maximum aspect ratio was set intuitively, based on the fact that the time of fabrication was severely limited, and optimization of the DRIE etching process for higher aspect ratios may not be possible. The clear aperture was kept as large as possible to widen the aperture size of the scanning system, and therefore, enhance resolution. Outer frame dimensions and the spring parameters were determined via extensive FEA simulations. The resulting dimensions were optimized for maximum displacement at around 11 KHz resonant frequency range. To be able to generate enough force from the actuators, 3 stage of comb sets fingers were placed on either side of the devices. This, of course, tripled the available electrostatic force, compared to a single stage actuator.

The inner frame bears no actuators and is excited by mechanical coupling to the outer frame motion. The two sets of comb actuators at both sides of the device enables differential driving to linearize the actuation force with respect to the applied voltage.

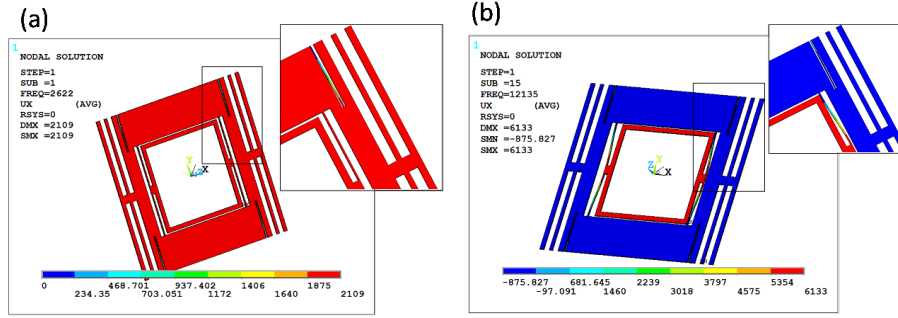


Figure 5.25: The shape of the mechanical operation eigenmodes of the micro-optical scanner. (a) Lower vibration mode: The inner and outer frames move in- phase with same deflection amplitude. (b) Higher vibration mode: The outer frame and inner frame motions are out-of-phase with an amplitude ratio of 7.8.

Unlike the single frame scanners, 2-DOF (degree-of-freedom) systems have two natural resonance frequencies at each fundamental vibration modes. At the lower vibration frequency, the inner and outer frames moves in-phase while at the higher vibration frequency, the motion of the frames is out- of- phase. Mechanical stroke multiplication can be obtained at either of the resonance frequencies of the system depending on the mass and stiffness of the frames and the corresponding flexures. The parameters of the scanner presented in this article are defined to obtain mechanical coupling at the higher vibration frequency of the in-plane sliding mode. In 5.25, the fundamental operation modes of the device are presented. As illustrated in 5.25a, at the lower vibration frequency, the inner and outer frames move in-phase (IP) and deflect by nearly same amount. At the higher vibration frequency (5.25b), the respective motions of the inner and the outer frame are out-of-phase (OOP). Comparing the motion amplitudes at this frequency shows that a small movement of the outer frame is coupled into and amplified at the inner frame. The amplification factor, the coupling-factor, is related to the stiffness and mass of the inner and outer frames.

5.4.2 Fabrication

The outline of the three mask fabrication process used is shown in Figure 3. As starting material silicon-on-insulator (SOI) wafers with a highly doped 20 m thick device layer were used. The doping is needed to maintain conductivity. In addition to the buried oxide also the backside of the wafer is oxidized from the outset (5.26.1). For the definition of wire-

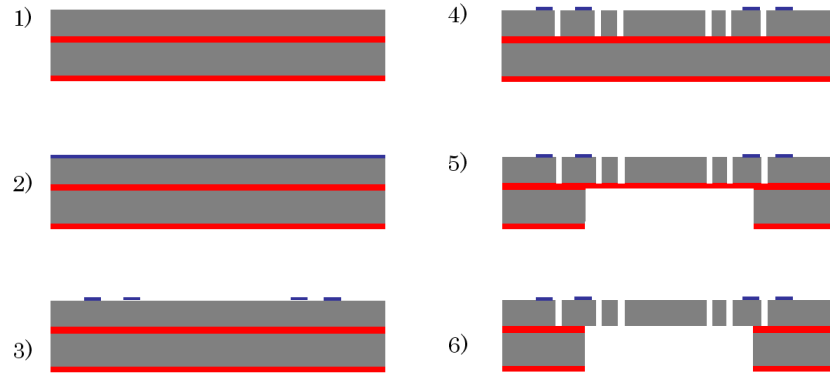


Figure 5.26: Fabrication process for the micro-optical scanner.

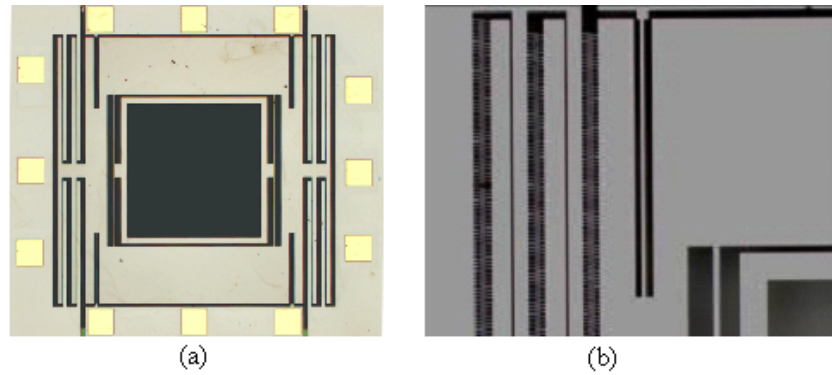


Figure 5.27: Microscope picture of a fabricated micro-optical scanner. (b) SEM picture of comb actuators. Comb width and comb pitch are $2\ \mu\text{m}$ and $12\ \mu\text{m}$, respectively. The fingers are $35\ \mu\text{m}$ with $20\ \mu\text{m}$ initial overlap.

bond electrodes, Ti/Au metal layers ($20\ \text{nm}/120\ \text{nm}$) are sputtered on the structural layer (5.26.2) and then patterned by photolithography (5.26.3). Next, the structural elements are defined through patterning and a DRIE step (5.26.4). To create the backside opening a final patterning step is performed, followed by a DRIE etch-step where the backside oxide is used as the etch mask (5.26.5). As a last step the devices are released through an oxide etch (5.26.6). Microscope and SEM pictures of the fabricated devices are presented in Figure 4.

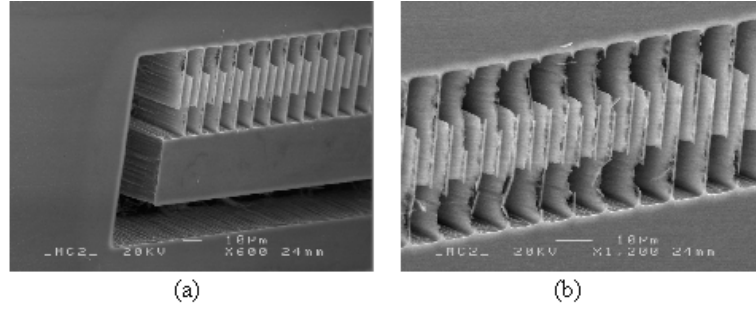


Figure 5.28: SEM pictures of the micro-optical scanner. The picture on the right shows the comb actuators of a properly fabricated device with minimal side etch.

5.4.3 Analytical Modeling

A convenient way of obtaining equations of motion is to use Euler-Lagrange method, where potential and kinetic energies for each component in the system are inspected. Defining the Lagrangian as:

$$L = T - V \quad (5.18)$$

where L is the Lagrangian, and T and V are the kinetic and potential energies in the system, respectively. One can write the equations of motion as follows:

$$\frac{d}{dt} \left(\frac{\partial L}{\partial \dot{q}_i} \right) - \frac{\partial L}{\partial q_i} = F_i \quad (5.19)$$

where, F_i is the net force and ignores the viscous damping on the system. For the system sketched in Figure 5.24, the two variables are chosen as, $q_1 = x_1$ and $q_2 = x_2$, to the left hand side of 5.19. x_1 and x_2 represent the deflection of the inner and outer frame, respectively. Assuming a linear system, total kinetic energy, stemming from the inertial forces on the mirror (inner frame) and the outer frame is given as:

$$T = \frac{1}{2} m_1 \dot{x}_1^2 + \frac{1}{2} m_2 \dot{x}_2^2 \quad (5.20)$$

m_1 and m_2 are the mass of the inner and the outer frame. The total mass of the device, denoted by m_t , is given by $m_t = m_1 + m_2$. The potential energy energy stored in the springs is:

$$V = \frac{1}{2} k_1 (x_1 - x_2)^2 + \frac{1}{2} k_2 x_2^2 \quad (5.21)$$

where k_1 and k_2 are the spring stiffness for the inner and outer flexures, respectively. Effective values of the stiffness terms can be calculated using tabulated formulas [77]. Substituting 5.20 and 5.24 into 5.19 yields:

$$L = \frac{1}{2} \left(m_1 \dot{x}_1^2 + m_2 \dot{x}_2^2 - k_1 (x_1 - x_2)^2 - \frac{1}{2} k_2 x_2^2 \right) \quad (5.22)$$

Introducing the damping terms, which are linearly dependent on the velocity as $b_i \dot{q}_i$, to the right-hand side of (6), the following equations can be derived:

$$m_1 \ddot{x}_1 + b_1 \dot{x}_1 + k_1 (x_1 - x_2) = 0 \quad (5.23)$$

$$m_2 \ddot{x}_2 + b_2 \dot{x}_2 + (k_1 + k_2) x_2 = F_e + k_1 x_1$$

where b_i are the viscous damping factors and F_e is the electrostatic force generated by the comb actuators. The damping coefficients used in the simulations are determined experimentally. Numerical values of the parameters in the above equation for the device shown in Figure 5.24 are listed in Table 5.4. The natural frequencies ω_1 and ω_2 of each frame acting alone are defined using the stiffness and mass parameters of the inner and outer frames:

$$\omega_1 = \sqrt{\frac{k_1}{m_1}}, \quad \omega_2 = \sqrt{\frac{k_2}{m_2}} \quad (5.24)$$

The frequency response of the system defined by 5.24 with parameters in Table 5.4 is plotted in Figure 5.29 for the displacements x_1 and x_2 . The system has two resonances along the in-phase direction. At the lower vibration mode of the MEMS platform, inner and outer frames oscillate in-phase with nearly identical amplitudes. In this design, higher vibration mode motion of the two cascaded frames is, however, out-of-phase with an amplitude ratio given by the coupling-factor. The lower vibration mode (IP-resonance mode) and higher vibration mode (OOP-resonance mode) resonance peaks are defined by the mass and spring constant of the frames and the flexures. The coupling-factor $M = x_1/x_2$ can be defined in the frequency domain using 5.24:

$$M(\omega) = \left| \frac{X_1(\omega)}{X_2(\omega)} \right| = \frac{k_1}{\sqrt{(k_1 - m_1 \omega^2)^2 + (b_1 \omega)^2}} \quad (5.25)$$

where, ω is the excitation frequency. The in-phase and out-of-phase resonance frequencies of the system can be solved and represented by ω_{IP} and ω_{OP} , which are found to be.

$$\omega_{IP}^2 = \frac{k_1}{2m_1} + \frac{\omega_1^2 + \omega_2^2}{2} - \frac{\sqrt{-4k_1 k_2 m_1 m_2 + (k_2 m_1 + k_1 m_1 + k_1 m_2)^2}}{2m_1 m_2} \quad (5.26)$$

Parameter	$m_1(kg)$	$m_2(kg)$	$b_1(Ns/m)$	$b_2(Ns/m)$	$k_1(N/m)$	$k_2(N/m)$
Value	2.3e-8	1.9e-7	2-3e-6	$6.8e-6$	108	60

Table 5.4: Numerical values of the system parameters

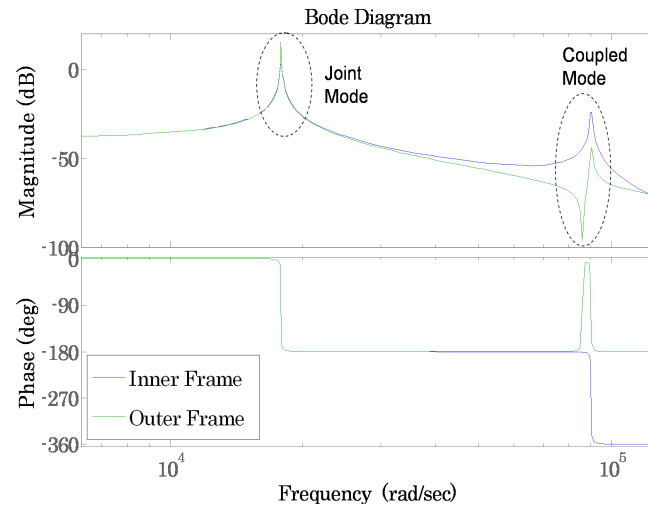


Figure 5.29: Typical amplitude and phase of the harmonic response of the mechanically coupled micro-optical scanner. At the joint mode resonance, the inner frame behaves as it is rigidly connected to the outer frame. At the coupled mode resonance, inner and outer frames movements are out-of phase.

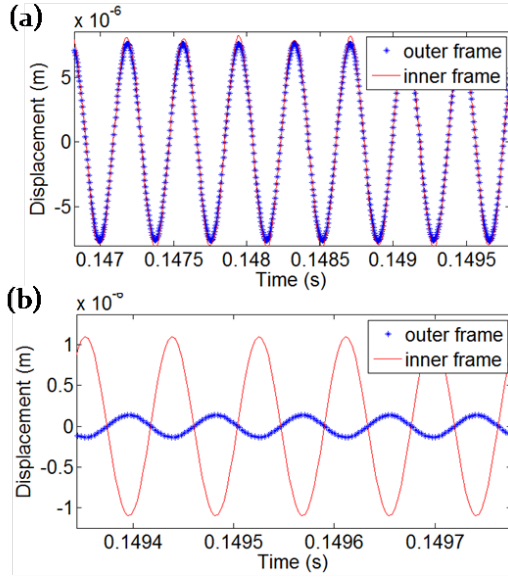


Figure 5.30: Simulated steady-state response of the system at the (a) in-phase and (b) out-of-phase resonance modes. Simulation are performed by numerically solving (5.19) with the parameters given in Table 5.4

$$\omega_{OP}^2 = \frac{k_1}{2m_1} + \frac{\omega_1^2 + \omega_2^2}{2} + \frac{\sqrt{-4k_1k_2m_1m_2 + (k_2m_1 + k_1m_1 + k_1m_t)^2}}{2m_1m_2} \quad (5.27)$$

Transient analysis of the device is presented in Figure 5.30 performed using MATLAB ODE solver at the in-phase resonance frequency with 30 V_{pp} voltages and at the out-of-phase resonance frequency with 15 V_{pp} voltages. Numerically obtained in-phase and out-of-phase resonance frequencies are 2.6 kHz and 11.57 kHz, respectively. As illustrated in Figure 5.30, at the in-phase resonance mode, the expected deflection amplitude is 8 μm . At the out-of-phase resonance mode, the expected inner frame deflection amplitude is 1.1 μm while the coupling efficiency is found to be 7.8.

5.4.4 Optimization

In order to be able to utilize the advantages of mechanical coupling phenomenon, one should carefully optimize the system parameters for the desired characteristics. For many scanning applications, this optimization translates into the maximization of travel range of a certain component with certain mass. (or inertia) and a predetermined scanning frequency. Such an optimization procedure is presented here. To maximize the inner frame deflection, the

effect of the outer frame mass and the outer flexure stiffness are analyzed. While the mass of the inner frame is not changed, the inner flexure stiffness is recalculated to achieve the predefined out-of-phase resonance frequency with different values of the outer frame mass and the outer flexure stiffness.

The coupling- factor and the deflection of the inner frame (in arbitrary units) are calculated with respect to the ratio of the device mass to the inner frame mass ($R_m = m_t/m_1 > 1$) and the outer flexure stiffness (k_2). The result is given as a contour plot in Figure 5.31. While the outer frame resonance frequency decreases, the inner flexure stiffness (k_1) increases to achieve the predetermined out-of-phase resonance frequency (ω_{OP}). Higher coupling-factor is obtained with higher k_1 ; However, the maximum achievable deflection of the inner frame decreases due to the excessive stiffness of the inner frame flexures (5.31b).

Coupling- factor is small in the boundaries where maximum deflection amplitude is achieved. Since the coupling-factor is beneficial due to lateral pull-in and air damping, the parameters must be defined to optimize the system performance in order to achieve sufficient deflection and considerable coupling- factor.

R_m and the outer flexure stiffness of the sample scanner, defined by the parameters illustrated in Table 5.4, are 8.5 and 60 N/m, respectively and marked on Figure 5.31. The scanner design has a significant coupling- factor with sufficient inner frame deflection.

It should be noted that, the mass of the outer frame can not be reduced drastically to preserve the necessary outer frame geometry around the inner frame and to have a significant coupling-factor.

When the inner frame resonance frequency is lower than the outer frame resonance frequency, mechanical coupling can be obtained at the lower vibration frequency (in-phase resonance mode) of the device, as well. Therefore, at the in-phase resonance frequency of the scanner, frames moves in phase and the deflection of the outer frame is transferred to the inner frame with mechanical stroke multiplication. Same numerical analyses are performed to optimize the 2-DoF systems which oscillate at the in-phase resonance frequency. In the analyses, inner frame mass (m_1) and in-phase resonance frequency are predefined while the outer frame mass and outer flexure stiffness are varying.

In this case, higher coupling-factor can be obtained increasing the outer frame resonance frequency of the device. However, as in the first analyses, the maximum achievable deflection

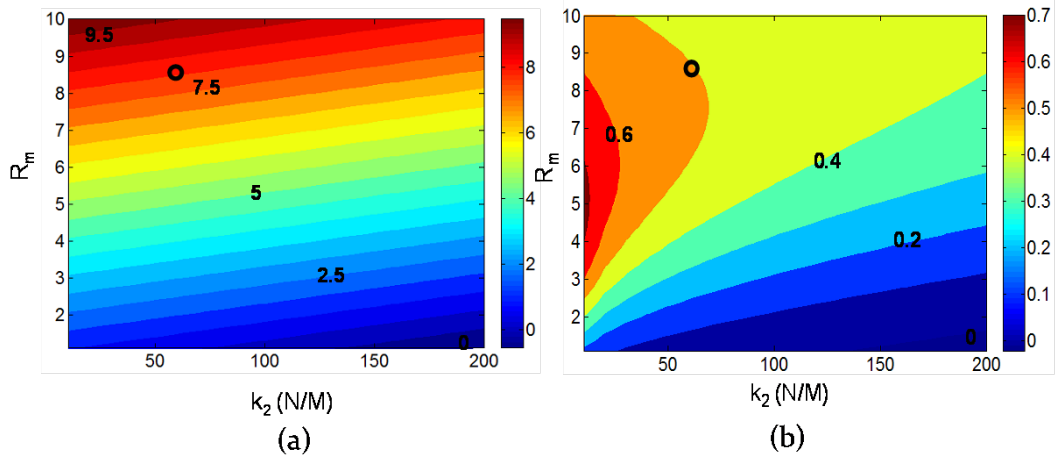


Figure 5.31: Out-of-phase resonance mode: Effect of the outer frame mass and outer flexure stiffness on (a) Coupling- factor. (b) Inner frame deflection.

decreases while the coupling- factor increases.

Depending on the required oscillation frequency, 2-DoF systems can be designed to have mechanical coupling in either in-phase resonance mode or out-of-phase resonance mode. Coupling mechanism utilized at both of the modes must be optimized to increase the system performance by analyzing the effect of the device parameters on the coupling- factor and the maximum achievable deflection amplitude.

5.4.5 Mechanical Performance

Comb drive scanner designs are tested to validate the mechanical coupling mechanism. Complementary biased sinusoids are applied to comb fingers on either side of the outer frame for differential driving to ensure harmonic excitation. In the OOP-resonance mode of the scanner, $0.9 \mu\text{m}$ inner frame deflection is obtained with $15 V_{pp}$ excitation voltages at 11.51 KHz. The coupling-factor is found to be 5. The maximum voltage applied to the scanner was $100 V_{pp}$ and $9 \mu\text{m}$ deflections is obtained at 11.51 KHz. For the IP-resonance mode at 2.5 KHz, $30 V_{pp}$ input results in $6 \mu\text{m}$ deflection. The experimentally achieved deflections agree with the numerically obtained values. Only slight difference is found due to fabrication variations and some broken comb fingers.

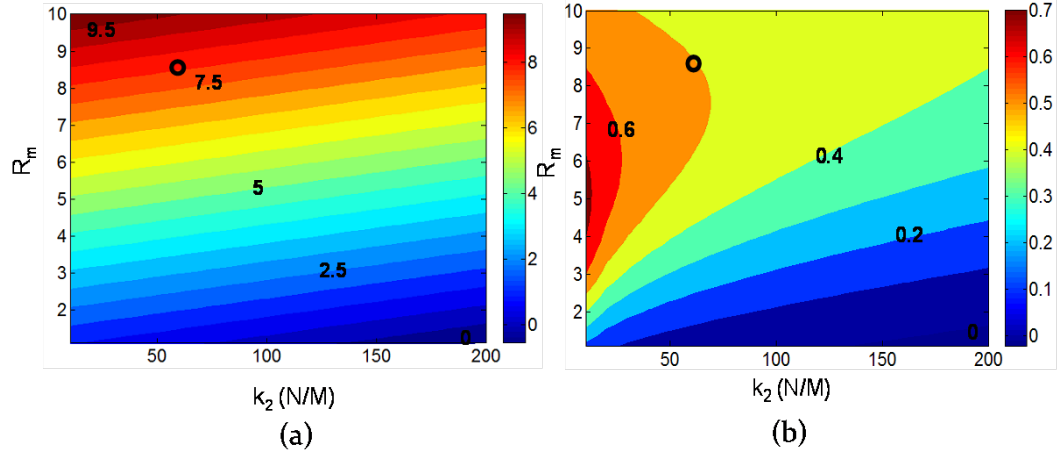


Figure 5.32: In-phase resonance mode: Effect of the outer frame mass and outer flexure stiffness on (a) Coupling- factor. (b) Inner frame deflection.

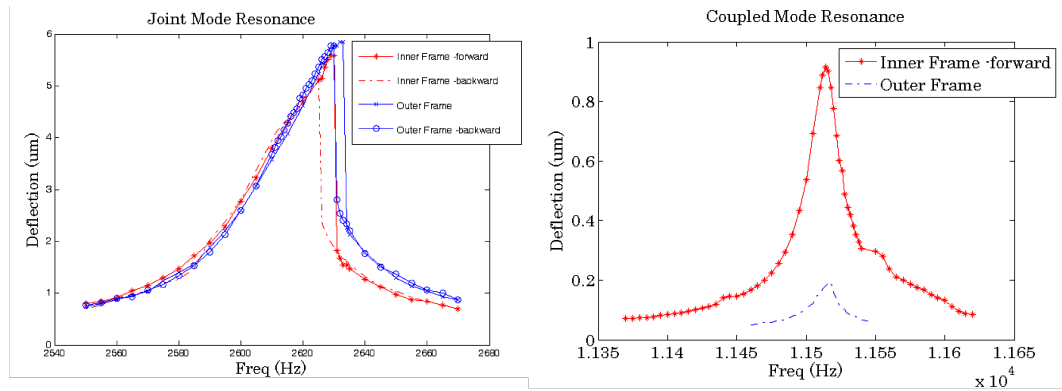


Figure 5.33: Due to differential driving, the frequency behavior of the device is expected to be linear, which is the case for around coupled mode resonance. However, around the lower vibration mode, the behavior is hysteretic; but, this hysteresis is different from parametric systems. This nonlinearity is due to the spring stiffening effect for large deflection values.

5.5 Remarks

In this chapter, a novel laser scanning system based on lateral translation of microlens arrays was presented. A simple solution is formulated and demonstrated to overcome the discrete addressing problem encountered in beam steering systems that use microlens arrays (the basic principle can be extended to be used in micromirror array based systems). The solution presented eliminates a serious limitation of such systems and can be applied anytime a periodic structure with tilted wavefront incidence is encountered. The system demonstrated in this work uses a PSL that moves together with the MMLA subject to a phase condition given and achieved almost 200 pixel continuous addressability using 1 mm clear aperture, 200 μm diameter, f/5 microlenses mounted on simple CD actuators. The proposed laser scanning system can be used for high-resolution and high-speed 2D imaging applications using two moving components mounted on xy-stages. The disadvantages of the system lies in the additional complexity of driving and control of two synchronously moving elements for single-axis scanning. A novel comb actuated micro-optical scanner concept was also developed to be employed for the presented scanning scheme. The device features two cascaded frames. Comb actuators are placed on both vertical (y) edges of the outer frame and lay along the x axis. Inner frame bears no actuators and is actuated by mechanical coupling of the outer frame motion. "Translation along y" mode resonance frequency of the inner frame is set 3-6 times larger than that of the outer frame. Small off-resonance motion of the outer frame couples to the inner frame, and gets amplified due to the shape of the mechanical resonance utilizing the mechanical coupling. Mechanical coupling can be obtained either at the lower or at the higher resonance frequencies of the devices depending on the inner and outer frame parameters. The mechanical coupling phenomenon is utilized in the OOP- resonance mode of the scanner presented in this article. The main advantage of this type of actuation is the large stroke achievable at high frequencies, which is very challenging task in MEMS design. With this method, (9 μm) deflection was obtained at a frequency higher than 10 KHz.

Chapter 6

CONCLUSIONS

Two novel methods for Fourier transform spectroscopy and laser scanned imaging are developed. In addition to the design, fabrication and characterization of novel optical MEMS components required to implement those methods, several aspects of spectrometer and laser scanner system design.

A major part of this thesis work focuses on the design, integration, and characterization of a novel miniature FT-IR spectrometer with a comb actuated micro-electro-mechanical component constituting its basic building block. The MEMS component utilizes vertical resonant comb drives for actuation, light dispersion, and optical path difference monitoring. This is a novel platform for Fourier spectroscopy developed within this thesis work and subject to European patent EP1677086. The patent is now licensed by Fraunhofer IPMS, Germany. The fundamental advantages offered by the developed spectrometer system over the existing Fourier spectrometers are high light efficiency (wide clear aperture), mechanical robustness, compactness, ease of fabrication and high integration level. A full FTS system can simply be implemented using the comb-actuated MEMS device and a single detector.

Design, fabrication and characterization of an out-of-plane vertical comb-drive actuator based Fourier transform spectrometer with 3mm x 3mm clear optical aperture is discussed. The device is capable of producing $\pm 52 \mu\text{m}$ vertical deflection at 28V square-wave excitation without requiring vacuum packaging. This performance level yields a theoretical spectral resolution of about 0.4 nm in the visible band and 3.6 nm at telecom wavelengths. The device is also suitable for spectroscopy in the mid and the long wave IR bands. The device fabrication is simple and uses standard SOI process with 3 masks and does not require additional IR optics. The only moving part is the resonating MEMS structure. Since the comb-fingers are used both for actuation and as dynamic diffraction grating, the die is not much larger than the optical aperture. This minimizes the size, complexity, cost, and the design effort of a fully functional system. An operational spectrometer using this

die requires integration with a simple photodetector and associated readout circuitry. The spectrometer has a number of unique features and important advantages compared to other MEMS spectrometers discussed earlier. However, static bending and dynamic deformation of the fingers due to thin structural layer, and moderate travel range made an updated new run necessary. Hence, a 2nd set of MEMS spectrometer devices were designed. The new designs share the same basic features with the devices of the 1st run, but are optimized to solve the problems associated with them.

An interferogram sampling method was implemented in order to overcome the sampling delay issues in Fourier spectrometers. In this system, both the laser fringe and the interferogram are sampled with separate ADCs synchronized by a single high frequency clock signal. The zero crossing times of the laser fringes are computed with a linear interpolation step, followed by a cubic interpolation of the interferogram to compute its value at the new found zero crossing time.

For the other major part of the project, a new laser scanning system based on lateral displacements of microlens arrays (MLA) is designed and demonstrated. The main advantages of the proposed scheme are the compactness and the high-resolution offered by only small deflections of the mechanical actuator. The novelty of the scheme lies in the method how the diffraction related limitations of MLA scanning systems are overcome using a prescan lens (PSL), and demonstrates continuous addressability with MLAs for the first time. Analytical and numerical results regarding the operation of the MLA scanning system and the use of PSL in addressing positions between discrete diffraction positions are verified by experimental results. The proposed scheme is particularly well-suited for forward-looking endoscopic imaging applications, where the optics and the actuator have to fit in a small tube, scanning laser-Doppler-vibrometry, and for applications where a large clear aperture and fast actuation is needed such as those in target detection and tracking in infrared. A set of proof-of-principle experiments were performed, and the validity of the scanning approach was demonstrated. The setup used for those experiments employed optical pick-up heads extracted from CD-ROM drivers for PSL and MLA translation. With this setup, a 1D scanning with more than 200 resolvable pixels was demonstrated using f/5 and 200 μm pitch size MLAs.

A novel comb actuated micro-optical scanner concept was developed to be utilized for

moving the PSL and the MLA in a fast and precise manner. The device features two cascaded frames. Comb actuators are placed on both vertical (y) edges of the outer frame and lay along the x axis. Inner frame bears no actuators and is actuated by mechanical coupling of the outer frame motion. “Translation along y” mode resonance frequency of the inner frame is set 3-6 times larger than that of the outer frame. If the outer frame is actuated at inner frame resonance, small off-resonance motion of the outer frame couples to the inner frame, and gets amplified due to the shape of the mechanical resonance. The main advantage of this type of actuation is the large stroke achievable at high frequencies, which is very challenging task in MEMS design. With this method, more than 10 μm resonant deflection was obtained at a frequency higher than 10 KHz.

Appendix A

INTERFEROGRAM AND TEMPORAL COHERENCE

A qualitative derivation of the Fourier transform relationship between the interferogram and the power spectral density was given in Section 4.2. Here, this relationship is analyzed rigorously within the context of temporal coherence. The discussion given here is edited from [135].

A.1 Temporal Coherence

A qualitative definition of temporal coherence can be depicted as follows: Let $\mathbf{u}(P, t)$ be the complex scalar representation of an optical disturbance at point P in space and instant t in time. Since $\mathbf{u}(P, t)$ is a real source with bandwidth $\Delta\sigma$, the complex envelope $\mathbf{A}(P, t)$ and phase of $\mathbf{u}(P, t)$ would vary in time at a rate determined by $\Delta\sigma$. For a finite time duration τ , $\mathbf{A}(P, t)$ is expected to remain relatively constant, provided that $\tau \ll 1/\Delta\sigma$. Therefore, the functions $\mathbf{A}(P, t)$ and $\mathbf{A}(P, t + \tau)$ are highly correlated, or *coherent* if τ is much less than the *coherence time* $\tau_c \cong 1/\Delta\sigma$. For a more quantitative definition, which will also reveal the mathematical relation between the spectrum and interferogram, temporal coherence will be treated in the context of a Michelson interferometer. Please note that, due to the duality of the Michelson and lamellar grating interferometers, the following treatment is also valid for the latter approach.

A.2 The Autocorrelation Function and the Wiener-Khinchin theorem

The *power spectral density* of a random process $u(t)$ is defined as

$$\mathcal{G}_u(\sigma) = \lim_{T \rightarrow \infty} \frac{E[\mathcal{U}_T \mathcal{U}_T^*]}{T} \quad (\text{A.1})$$

where E denotes the expectation function of the optical disturbance defined as a random process. $u(t)$ is a real-valued function, so that $\mathcal{U}_T^*(\sigma) = \mathcal{U}_T(-\sigma)$. It is further noted that

$$\mathcal{U}_T(\sigma) = \int_{-\infty}^{\infty} \text{rect}\left(\frac{\xi}{T}\right) u(\xi) e^{j2\pi\sigma\xi} d\xi \quad (\text{A.2})$$

$$\mathcal{U}_T(-\sigma) = \int_{-\infty}^{\infty} \text{rect}\left(\frac{\eta}{T}\right) u(\eta) e^{-j2\pi\sigma\eta} d\eta$$

Substituting A.3 in A.1 yields

$$\frac{E \left[|\mathcal{U}_T(\sigma)|^2 \right]}{T} = \frac{1}{T} \int \int_{-\infty}^{\infty} \text{rect}\left(\frac{\xi}{T}\right) \text{rect}\left(\frac{\eta}{T}\right) E[u(\xi)u(\eta)] e^{j2\pi\sigma(\xi-\eta)} d\xi d\eta. \quad (\text{A.3})$$

The expectation is recognized as the statistical autocorrelation function of $u(t)$; therefore

$$\frac{E \left[|\mathcal{U}_T(\sigma)|^2 \right]}{T} = \frac{1}{T} \int \int_{-\infty}^{\infty} \text{rect}\left(\frac{\xi}{T}\right) \text{rect}\left(\frac{\eta}{T}\right) \Gamma_u(\xi, \eta) e^{j2\pi\sigma(\xi-\eta)} d\xi d\eta. \quad (\text{A.4})$$

With a simple change of variables, with $\xi \rightarrow t + \tau$ and $\eta \rightarrow t$, one obtains

$$\frac{E \left[|\mathcal{U}_T(\sigma)|^2 \right]}{T} = \frac{1}{T} \int \int_{-\infty}^{\infty} \text{rect}\left(\frac{t+\tau}{T}\right) \text{rect}\left(\frac{t}{T}\right) \Gamma_u(t+\tau, t) e^{j2\pi\sigma\tau} dt d\tau. \quad (\text{A.5})$$

The power spectral density is the limit of this expression as $T \rightarrow \infty$. Interchanging orders of integration with respect to τ and the limit, and noting that for any fixed τ

$$\lim_{T \rightarrow \infty} \frac{1}{T} \int_{-\infty}^{\infty} \text{rect}\left(\frac{t+\tau}{T}\right) \text{rect}\left(\frac{t}{T}\right) \Gamma(t+\tau, t) dt = \langle \Gamma(t+\tau, t) \rangle, \quad (\text{A.6})$$

one can obtain

$$\mathcal{G}_u(\sigma) = \int_{-\infty}^{\infty} \langle \Gamma(t+\tau, t) \rangle e^{j2\pi\sigma\tau} d\tau, \quad (\text{A.7})$$

where, the angle brackets represent the time averaging. This important result is called the *Wiener-Kinchin theorem*, and shows that the power spectral density of any random process can be found from a Fourier transform of a suitably averaged autocorrelation function.

A.3 Fringe Visibility and Temporal Coherence

For all practical spectroscopy applications, the detector can be assumed to average over an infinitely long time duration, due to the tremendous difference between the optical frequency of the measured light and interferogram modulation frequency. Assuming that there is a OPD of ρ between the two arms of a Michelson interferometer, the intensity incident on the detector can be expressed as

$$I_D(\rho) = \left\langle \left| \mathbf{u}(t) + \mathbf{u}\left(t + \frac{\rho}{c}\right) \right|^2 \right\rangle, \quad (\text{A.8})$$

where $\mathbf{u}(t)$ is the analytical representation of the source. Expanding this expression yields

$$I_D(\rho) = \left\langle |\mathbf{u}(t)|^2 \right\rangle + \left\langle \left| \mathbf{u}\left(t + \frac{\rho}{c}\right) \right|^2 \right\rangle + \left\langle \mathbf{u}\left(t + \frac{\rho}{c}\right) \mathbf{u}^*(t) \right\rangle + \left\langle \mathbf{u}^*\left(t + \frac{\rho}{c}\right) \mathbf{u}(t) \right\rangle, \quad (\text{A.9})$$

Due to the important role of the time averages, the following notations will be used from now on:

$$I_0 \langle |\mathbf{u}(t)|^2 \rangle = \left\langle \left| \mathbf{u}\left(t + \frac{\rho}{c}\right) \right|^2 \right\rangle \quad (\text{A.10})$$

and

$$\Gamma(\tau) = \left\langle \mathbf{u}\left(t + \frac{\rho}{c}\right) \mathbf{u}^*(t) \right\rangle. \quad (\text{A.11})$$

The autocorrelation function $\Gamma(\tau)$ of the signal $\mathbf{u}(t)$ is called the *self coherence function* of the optical disturbance. In this new notation, the detected intensity is written as

$$\begin{aligned} I_d &= I_0 + \Gamma\left(\frac{\rho}{c}\right) + \Gamma^*\left(\frac{\rho}{c}\right) \\ &= I_0 + 2\Re \left\{ \Gamma\left(\frac{\rho}{c}\right) \right\}. \end{aligned} \quad (\text{A.12})$$

In most occasions, it is more convenient to work with the normalized version of the self coherence function, which is known as the *complex degree of coherence*, and defined as

$$\gamma(\tau) = \frac{\Gamma(\tau)}{\Gamma(0)}, \quad (\text{A.13})$$

It readily follows from this definition that $\gamma(0) = 1$ and $|\gamma(\tau)| \leq 1$. The detector intensity is now given by

$$I_D(\rho) = 2I_0 \left[1 + 2\Re \left\{ \gamma\left(\frac{\rho}{c}\right) \right\} \right]. \quad (\text{A.14})$$

In order to be able to reach an analytic expression describing the interferogram, the complex degree of coherence is expressed in the following general form:

$$\gamma(\tau) = |\gamma(\tau)| \exp \{ -j(2\pi\sigma_0\tau - \alpha(\tau)) \}, \quad (\text{A.15})$$

where σ is the center frequency of the light and $\alpha(\tau) = \arg \gamma(\tau) + 2\pi\sigma_0\tau$. Using this expression, the interferogram can be expressed as:

$$I_D(\rho) = 2I_0 \left\{ 1 + \left| \gamma\left(\frac{\rho}{c}\right) \right| \cos \left(2\pi\left(\frac{\rho}{\lambda}\right) - \alpha\left(\frac{\rho}{c}\right) \right) \right\}. \quad (\text{A.16})$$

In the case of zero OPD ($\rho = 0$), $|\gamma(\frac{\rho}{c})| \cong 1$ and $\alpha(\rho/c) \cong 0$; therefore, the interferogram consists of a fully modulated cosine. As the OPD is increased, the amplitude modulation of the interferogram falls from unity to zero. The modulation depth of the fringes can be precisely described with the term *fringe visibility* defined by

$$\mathcal{V} = \frac{I_{\max} - I_{\min}}{I_{\max} + I_{\min}} \quad (\text{A.17})$$

where I_{max} and I_{min} are the intensities at the maximum and minimum of the fringe. Within the close neighborhood of OPD τ , the fringe visibility is

$$\vartheta(\rho) = \left| \gamma \left(\frac{\rho}{c} \right) \right| \quad (\text{A.18})$$

If the optical losses of the two interferometer arms are not equal, it can easily be shown that the fringe visibility is equal to

$$\vartheta(\rho) = \frac{2K_1K_2}{K_1^2 + K_2^2} \left| \gamma \left(\frac{\rho}{c} \right) \right| \quad (\text{A.19})$$

where K_1 and K_2 are the losses of the two interferometer arms. It can be noted that A.17 has the same form as 4.36 defined to express the modulation depth of an interferogram obtained from an LGI. The drop in the modulation depth for large OPD values can be modeled as a Michelson interferometer with a path dependent loss at one arm.

A.4 Interferogram and The Power Spectral Density

The concept of temporal coherence has to do with the ability of two relatively delayed light beams to form fringes. Section A.2 established that, for a stationary random process, the Wiener-Kinchin theorem suggests that a Fourier transform relation exists between the autocorrelation function and the power spectral density. In particular,

$$\Gamma(\tau) = \int_{-\infty}^{\infty} 4B(\sigma)e^{-j2\pi\sigma\tau}d\sigma, \quad (\text{A.20})$$

where $B(\sigma)$ is the power spectral density of the real source $u(\sigma)$. Equivalently, the complex degree of coherence $\gamma(\tau)$ in terms of $B(\sigma)$ is given by

$$\gamma(\tau) = \frac{\int_{-\infty}^{\infty} 4B(\sigma)e^{-j2\pi\sigma\tau}d\sigma}{\int_{-\infty}^{\infty} 4B(\sigma)d\sigma} = \int_{-\infty}^{\infty} \hat{B}(\sigma)e^{-j2\pi\sigma\tau}d\sigma \quad (\text{A.21})$$

where $\hat{B}(\sigma)$ is the normalized power spectral density.

Appendix B

SCALAR DIFFRACTION THEORY

Diffraction, as it first defined by Sommerfeld, refers to “any deviation of light rays from rectilinear paths which cannot be interpreted as reflection or refraction“. It is caused by the spatial confinement of a wave, and is strongest when that confinement is in the order of the wavelength. A complete description of diffraction requires a rigorous solution of Maxwell’s equations, which are defined by vector quantities. However, for linear, and homogeneous media, it was experimentally shown that all three components of the vector fields behave identically; hence a scalar definition of diffraction is possible. In this section, such a scalar diffraction theory is established. The treatment presented here follows the comprehensive analysis given in [82].

B.1 Rayleigh-Sommerfeld Diffraction Theory*B.1.1 From Vector to Scalar Theory*

In MKS units and in the absence of free charge, Maxwell’s equations are given by

$$\begin{aligned}\Delta \times \vec{\mathcal{E}} &= -\mu \frac{\partial \vec{\mathcal{H}}}{\partial t} \\ \Delta \times \vec{\mathcal{H}} &= \epsilon \frac{\partial \vec{\mathcal{E}}}{\partial t} \\ \Delta \cdot \epsilon \vec{\mathcal{E}} &= 0 \\ \Delta \cdot \mu \vec{\mathcal{H}} &= 0,\end{aligned}\tag{B.1}$$

where $\vec{\mathcal{E}}$ is the electric field, $\vec{\mathcal{H}}$ is the magnetic field, and μ and ϵ are the permeability and permittivity of the medium of propagation, respectively. Now assume that the propagation medium is linear, isotropic, homogeneous, and nondispersive. Using the vector identity $\Delta \times (\Delta \times \vec{\mathcal{E}}) = \Delta(\Delta \cdot \vec{\mathcal{E}}) - \Delta^2 \vec{\mathcal{E}}$, cross product of the right and left sides of the first equation with the gradient operator Δ , therefore yields

$$\Delta^2 \vec{\mathcal{E}} - \frac{n^2}{c^2} \frac{\partial^2 \vec{\mathcal{E}}}{\partial t^2}\tag{B.2}$$

where n is the refractive index of the medium defined by $n = \sqrt{\frac{\epsilon}{\epsilon_0}}$ (ϵ_0 , and c are the vacuum permittivity and velocity of propagation in vacuum, respectively). Following the same procedure, an identical relation can be also be derived for the magnetic field. Equation B.2 is called the *vector wave equation* and is obeyed by both $\vec{\mathcal{E}}$ and $\vec{\mathcal{H}}$. In a linear, isotropic, homogeneous, nondispersive medium, all three components of the fields are independent from each other. Therefore, it is possible to summarize the behavior of all these components through a single scalar wave equation,

$$\Delta^2 u(P, t) - \frac{n^2}{c^2} \frac{\partial^2 u(P, t)}{\partial t^2} \quad (\text{B.3})$$

where $u(P, t)$ represents any of the scalar field components, which are functions of both position P and time t .

In the case of diffraction of light by an aperture, the $\vec{\mathcal{E}}$ and $\vec{\mathcal{H}}$ fields are modified only at the edges of the aperture where light interacts with the material of which the edges are composed, and the effects extend over only a few wavelengths into the aperture itself. Thus if the aperture has an area that is large compared with a wavelength, the coupling effects of the boundary conditions on the $\vec{\mathcal{E}}$ and $\vec{\mathcal{H}}$ fields will be small, and scalar approximation is valid.

B.1.2 The Helmholtz Equation

Let the light disturbance at position P and time t be represented by a scalar function $u(P, t)$. then, this scalar field can be writtes as

$$u(P, t) = \text{Re} \{ U(P) \exp(-j2\pi\nu t) \}, \quad (\text{B.4})$$

where $U(P)$ is a complex function of position called the *phasor*, and is given by

$$U(P) = A(P) \exp(-j\phi(P)). \quad (\text{B.5})$$

If the real disturbance $u(P, t)$ represents a radiation wave, then it satisfies the scallar wave equatin given in B.3 at each point without a source. Substituting B.4 into the scalar wave equation, one can find that U obeys the following time-independent equation:

$$(\Delta^2 + k^2)U = 0. \quad (\text{B.6})$$

where, k is the wavenumber given by $k = 2\pi n\nu/c = 2\pi/\lambda$. This relation is known as the *Helmholtz equation*.

B.1.3 Green's Theorem

The field amplitude of a complex disturbance U at an observation point P in space, can be calculated using the famous mathematical relation known as *Green's theorem*, which can be stated as follows:

Let $U(P)$ and $G(P)$ be any two complex-valued functions of position, and let S be a closed surface surrounding a volume V . If U , G , and their first and second partial derivatives are single-valued and continuous within and on S , then we have

$$\int \int_V (U \Delta^2 G - G \Delta^2 U) dv = \int \int_S \left(U \frac{\partial G}{\partial n} - G \frac{\partial U}{\partial n} \right) ds \quad (\text{B.7})$$

where $\frac{\partial}{\partial n}$ is a partial derivative in the outward normal direction at each point on S

Suppose that we wish to solve an inhomogeneous linear differential equation of the form

$$a_2(x) \frac{d^2 U}{dx^2} + a_1(x) \frac{dU}{dx} + a_0(x) U = V(x) \quad (\text{B.8})$$

where $V(x)$ is a driving function and $U(x)$ satisfies a known set of boundary conditions. It can be shown that $G(x)$ is the solution to the same differential equation when $V(x)$ is replaced by the impulsive driving function $\delta(x - x')$ and with the same boundary conditions applying, then the general solution $U(x)$ can be expressed in terms of $G(x)$ through a convolution integral

$$U(x) = \int G(x - x') V(x') dx'. \quad (\text{B.9})$$

the function $G(x)$ is known as the *Green's function* of the problem. Various solutions to the scalar diffraction problem to be discussed in the following sections correspond to results obtained under different assumptions about the Green's function of the problem. The function G appearing in Green's theorem may be regarded either as simply an auxiliary function which we cleverly choose to solve our problem, or it may eventually be related to the Green's function of the problem. Green's theorem constitutes the basis for the scalar diffraction theory. Depending on the choice of the specific Green function, various diffraction formulations were developed. Here, the Rayleigh-Sommerfeld diffraction theory will be explored.

B.1.4 The Integral Theorem of Helmholtz and Kirchhoff

The Kirchhoff formulation of the diffraction problem is based on a certain integral theorem which expresses the solution of the homogeneous wave equation at an arbitrary point in

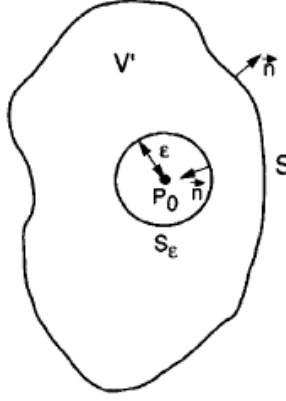


Figure B.1: Surface of Integration

terms of the values of the solution and its first derivative on an arbitrary closed surface surrounding that point. Let the point of observation be denoted P_0 , and let S denote an arbitrary closed surface surrounding P_0 , as indicated in B.1. The problem is to express the optical disturbance at P_0 in terms of its values on the surface S . To solve this problem, we follow Kirchhoff in applying Green's theorem and in choosing as an auxiliary function a unit-amplitude spherical wave expanding about the point P_0 . Thus the value of Kirchhoff's G at an arbitrary point P_1 is given by

$$G(P_1) = e^{jkr_{01}}/r_{01}, \quad (\text{B.10})$$

where r_{01} is the length of the vector pointing from P_0 to P_1 . The function G (as well as its first and second partial derivatives) must be continuous within the enclosed volume V . Therefore to exclude the discontinuity at P_0 , a small spherical surface S_ϵ , of radius ϵ , is inserted about the point P_0 . Green's theorem is then applied, the volume of integration V' being that volume lying between S and S_ϵ , and the surface of integration being the composite surface $S' = S + S_\epsilon$ as shown in Figure B.1. Within the volume V' , the disturbance G , being simply an expanding spherical wave, satisfies the Helmholtz equation

$$(\Delta^2 + k^2)G = 0. \quad (\text{B.11})$$

Substituting the two Helmholtz equations (3-13) and (3-18) in the left-hand side of Green's theorem, we find

$$\int \int_{V'} \int (U\Delta^2 G - G\Delta^2 U) dv = - \int \int_{V'} \int (UGk^2 - GUk^2) dv \equiv 0. \quad (\text{B.12})$$

Thus the theorem reduces to

$$\int \int_{S'} \left(U \frac{\partial G}{\partial n} - G \frac{\partial U}{\partial n} \right) ds = 0 \quad (\text{B.13})$$

which follows that

$$-\int \int_{S_\epsilon} \left(U \frac{\partial G}{\partial n} - G \frac{\partial U}{\partial n} \right) ds = -\int \int_S \left(U \frac{\partial G}{\partial n} - G \frac{\partial U}{\partial n} \right) ds = \quad (\text{B.14})$$

Note that, for a general point P_1 on S' , in addition to equation xx, we have

$$\frac{\partial G(P_1)}{\partial n} = \cos(\vec{n}, \vec{r}_{01}) \left(jk - \frac{1}{r_{01}} \right) e^{jk r_{01}} / r_{01}. \quad (\text{B.15})$$

where $\cos(\vec{n}, \vec{r}_{01})$ represents the cosine of the angle between the outward normal $\vec{n}$ and the vector $\vec{r}_{01}$ joining P_0 to P_1 . For the particular case of P_1 on S_ϵ , $\cos(\vec{n}, \vec{r}_{01}) = 1$, and these equations become

$$G(P_1) = \frac{e^{jk\epsilon}}{\epsilon}, \quad \frac{\partial G(P_1)}{\partial n} = \frac{e^{jk\epsilon}(\frac{1}{\epsilon} - jk)}{\epsilon} \quad (\text{B.16})$$

Letting ϵ become arbitrarily small, the continuity of U (and its derivatives) at P_0 allows us to write

$$\begin{aligned} \lim_{\epsilon \rightarrow 0} \int \int_{S_\epsilon} \left(U \frac{\partial G}{\partial n} - G \frac{\partial U}{\partial n} \right) ds &= \quad (\text{B.17}) \\ \lim_{\epsilon \rightarrow 0} 4\pi\epsilon^2 \left[U(P_0) \frac{e^{jk\epsilon}(\frac{1}{\epsilon} - jk)}{\epsilon} - \frac{\partial U(P_0)}{\partial n} \frac{e^{jk\epsilon}}{\epsilon} \right] &= 4\pi U(P_0). \end{aligned}$$

Substitution of this result in (3-19) (taking account of the negative sign) yields

$$U(P_0) = \frac{1}{4\pi} \int \int_S \left\{ \frac{\partial U}{\partial n} \left(\frac{e^{jk r_{01}}}{r_{01}} \right) - U \frac{\partial}{\partial n} \left(\frac{e^{jk r_{01}}}{r_{01}} \right) \right\} ds. \quad (\text{B.18})$$

This result is known as the integral theorem of Helmholtz and Kirchhoff, it plays an important role in the development of the scalar theory of diffraction, for it allows the field at any point P_0 to be expressed in terms of the *boundary values* of the wave on any closed surface surrounding that point. As we shall now see, such a relation is instrumental in the further development of scalar diffraction equations.

B.1.5 The Rayleigh-Sommerfeld Diffraction Formulation

Despite the remarkably accurate results the Kirchhoff theory yielded, there are certain internal inconsistencies in the theory. It is a well-known theorem of potential theory that if a 2D

potential function and its normal derivative vanish together along any finite curve segment, then that potential function must vanish over the entire plane. Similarly, if a solution of the three-dimensional wave equation vanishes on any finite surface element, it must vanish in all space. Thus the two Kirchhoff boundary conditions together imply that the field is zero everywhere behind the aperture, a result which contradicts the known physical situation. Sommerfeld has removed this internal inconsistencies,

Suppose G is generated not only by a point source located at P_0 , but also simultaneously by a second point source at a position $\tilde{P}_0$ which is the mirror image of P_0 on the opposite side of the screen. Let the source at $\tilde{P}_0$ be of the same wavelength λ as the source at P_0 , and suppose that the two sources are oscillating out of phase. The Green's function in this case is given by

$$G_-(P_1) = \frac{e^{jk r_{01}}}{r_{01}} - \frac{e^{jk \tilde{r}_{01}}}{\tilde{r}_{01}} \quad (\text{B.19})$$

Such a function vanishes on the plane aperture, leaving the following expression for the observed field:

$$U_1(P_0) = -\frac{1}{4\pi} \int \int_{\Sigma} U \frac{\partial G_-}{\partial n} ds. \quad (\text{B.20})$$

This solution is referred as the *first Rayleigh-Sommerfeld solution*. This solution can be further specified. Let $\tilde{r}_{01}$ be the distance from $\tilde{P}_0$ to P_1 . The corresponding normal derivative of G_- is

$$\frac{\partial G_-}{\partial n} = \cos(\vec{n}, \vec{r}_{01}) \left(jk - \frac{1}{r_{01}} \right) \frac{e^{jk r_{01}}}{r_{01}} - \cos(\vec{n}, \vec{\tilde{r}}_{01}) \left(jk - \frac{1}{\tilde{r}_{01}} \right) \frac{e^{jk \tilde{r}_{01}}}{\tilde{r}_{01}} \quad (\text{B.21})$$

Now, for P_1 on s_1 , $r_{01} = \tilde{r}_{01}$ and $\cos(\vec{n}, \vec{r}_{01}) = \cos(\vec{n}, \vec{\tilde{r}}_{01})$. Therefore, on that surface

$$\frac{\partial G_-}{\partial n} = 2 \cos(\vec{n}, \vec{r}_{01}) \left(jk - \frac{1}{r_{01}} \right) \frac{e^{jk r_{01}}}{r_{01}}. \quad (\text{B.22})$$

For $r_{01} \gg \lambda$, the second term above can be dropped, leaving

$$\frac{\partial G_-}{\partial n} = 2jk \cos(\vec{n}, \vec{r}_{01}) \frac{e^{jk r_{01}}}{r_{01}}. \quad (\text{B.23})$$

which is just twice the normal derivative of the Kirchhoff's Green's function. With this result, the first Rayleigh-Sommerfeld solution can be expressed in terms of the more simple Green's function used by Kirchhoff,

$$U_1(P_0) = -\frac{1}{2\pi} \int \int_{\Sigma} U \frac{\partial G}{\partial n} ds. \quad (\text{B.24})$$

An alternative and equally valid Green's function is found by allowing the two point sources to oscillate in phase, giving

$$G_+(P_1) = \frac{e^{jkr_{01}}}{r_{01}} + \frac{e^{jkr_{01}}}{\tilde{r}_{01}}. \quad (\text{B.25})$$

With a similar analysis, this leads to an expression for $U(P_0)$ in terms of the Green's function used by Kirchhoff,

$$U_2(P_0) = \frac{1}{2\pi} \int \int_{\Sigma} U \frac{\partial G}{\partial n} ds. \quad (\text{B.26})$$

If the Green's function G_- is substituted for G in (A.13), it follows that

$$U_1(P_0) = \frac{1}{j\lambda} \int \int_{S_1} U(P_1) \frac{e^{jkr_{01}}}{r_{01}} \cos(\vec{n}, \vec{r}_{01}) ds \quad (\text{B.27})$$

where it has been assumed that $r_{01} \gg A$. The Kirchhoff boundary conditions may now be applied to U alone, yielding the general result

$$U_1(P_0) = \frac{1}{2\pi} \int \int_{\Sigma} U(P_1) \frac{e^{jkr_{01}}}{r_{01}} \cos(\vec{n}, \vec{r}_{01}) ds \quad (\text{B.28})$$

No boundary condition needs to be applied to $\partial U / \partial n$; thus, the inconsistencies of the Kirchhoff theory are removed. This relation is called the *Rayleigh-Sommerfeld diffraction solution*, and can also be written as

$$U(P_0) = \frac{1}{j\lambda} \int \int_{\Sigma} U(P_1) \frac{e^{jkr_{01}}}{r_{01}} \cos \theta ds, \quad (\text{B.29})$$

where θ is the angle between the vectors $\vec{n}$ and $\vec{r}_{01}$.

B.2 Fresnel and Fraunhofer Diffraction

In the preceding chapter the results of scalar diffraction theory were presented in their most general forms. Attention is now turned to certain approximations to the general theory, approximations that will allow diffraction pattern calculations to be reduced to comparatively simple mathematical manipulations. These approximations, which are commonly made in many fields that deal with wave propagation, will be referred to as Fresnel and Fraunhofer approximations.

B.2.1 The Fresnel Approximation

To reduce the Huygens-Fresnel principle to a more simple and usable expression, we introduce approximations for the distance r_{01} between P_1 and P_0 (Figure B.2). The approximations are based on the binomial expansion of the square root in B.32. Let b be a number

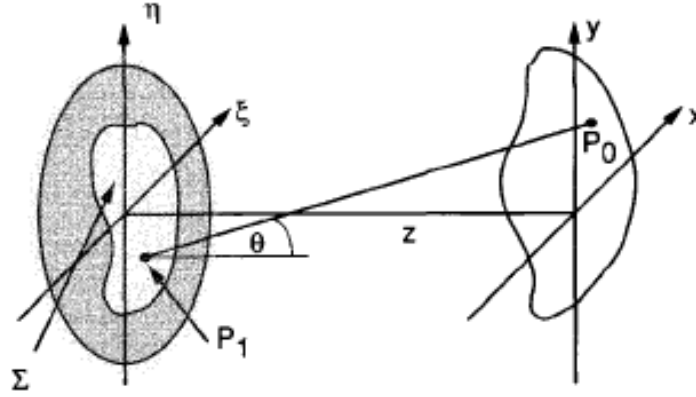


Figure B.2: Diffraction Geometry

that is less than unity. The binomial expansion of the square root is given by

$$\sqrt{1+b} = 1 + \frac{b}{2} + \frac{b^2}{8} + \dots, \quad (\text{B.30})$$

where the number of terms needed for a given accuracy depends on the magnitude of b . To apply the binomial expansion to the problem at hand, factor a z outside the expression for r_{01} , yielding

$$\begin{aligned} r_{01} &= z \sqrt{1 + \left(\frac{x-\xi}{z}\right)^2 + \left(\frac{y-\eta}{z}\right)^2} \\ &\approx z \left[1 + \frac{1}{2} \left(\frac{x-\xi}{z}\right)^2 + \frac{1}{2} \left(\frac{y-\eta}{z}\right)^2 \right]. \end{aligned} \quad (\text{B.31})$$

Let the quantity b in B.30 consist of the second and third terms under the square root in B.32. Then, retaining only the first two terms of the expansion, we have

$$U(x, y) = \frac{e^{jkz}}{j\lambda z} \int \int_{-\infty}^{\infty} U(\xi, \eta) \exp j \frac{k}{2z} [(x-\xi)^2 + (y-\eta)^2] d\xi d\eta, \quad (\text{B.32})$$

Another form of this is found if the quadratic phase term is factored outside the integral signs, yielding

$$U(x, y) = \frac{e^{jkz}}{j\lambda z} e^{j \frac{k}{2z} (x^2 + y^2)} \int \int_{-\infty}^{\infty} \left\{ U(\xi, \eta) e^{j \frac{k}{2z} (\xi^2 + \eta^2)} \right\} e^{-j \frac{k}{2z} (x\xi + y\eta)} d\xi d\eta, \quad (\text{B.33})$$

which is recognized (aside from multiplicative factors) to be the Fourier transform of the product of the complex field just to the right of the aperture and a quadratic phase exponential. *Accuracy of Fresnel Approximation* The accuracy of the Fresnel approximation is

determined by the errors induced when terms nonlinear with b are ignored in the binomial expansion. A sufficient condition would be that the maximum phase change induced by the $b^2/8$ term be much smaller than 1 radian. This condition would be met if the propagation distance z satisfies

$$z^3 \gg \frac{\pi}{4\lambda} \left[(x - \xi)^2 + (y - \eta)^2 \right]_{max}^2 \quad (\text{B.34})$$

For instance, for a 1cm circular aperture, and a wavelength of 0.5 μm , this condition indicates that z should be $\gg 25$ cm for accuracy. However, this condition is overly stringent, and good accuracy can be expected for much shorter distances.

B.2.2 The Fraunhofer Approximation

It was seen in the previous section that, in the region of Fresnel diffraction, the observed field strength $U(x, y)$ can be found from a Fourier transform of the product of the aperture distribution and a quadratic phase function. If in addition to the Fresnel approximation the stronger (*Fraunhofer*) approximation

$$z \gg \frac{k(\xi^2 + \eta^2)_{max}}{2} \quad (\text{B.35})$$

is satisfied, then the quadratic phase factor under the integral sign in (B.33) is approximately unity over the entire aperture, and the observed field strength can be found (up to a multiplicative phase factor) directly from a Fourier transform of the aperture distribution itself. Therefore, in the Fraunhofer diffraction region, or *far-field*, the diffraction profile is given by

$$U(x, y) = \frac{e^{jkz} e^{j\frac{k}{2z}(x^2+y^2)}}{j\lambda z} \int \int_{-\infty}^{\infty} U(\xi, \eta) \exp \left[-j\frac{2\pi}{\lambda z}(x\xi + y\eta) \right] d\xi d\eta. \quad (\text{B.36})$$

Aside from multiplicative phase factors preceding the integral, this expression is simply the Fourier transform of the aperture distribution, evaluated at frequencies

$$\begin{aligned} f_X &= \frac{x}{\lambda z} \\ f_Y &= \frac{y}{\lambda z} \end{aligned} \quad (\text{B.37})$$

A rule of thumb, known as the *antenna designer's formula*, states that for an aperture of linear dimension D , the Fraunhofer approximation will be valid provided

$$z \gg \frac{2D^2}{\lambda} \quad (\text{B.38})$$

For example, at a wavelength of $0.6\ \mu\text{m}$ (red light), and an aperture width of $50\ \mu\text{m}$, the observation distance must be slightly greater 8 mm.

Appendix C

PARAMETRICALLY EXCITED SYSTEMS

C.1 Introduction

A parametrically excited system is a second order differential equation, for which the time-dependent excitations appear as coefficients (parameters) of the equation of motion. In simple harmonic systems, a small excitation cannot create a large response unless the excitation frequency is close to the resonance frequency of the system (primary resonance). However, a small parametric excitation can produce a large response when frequency of the excitation is close to twice the natural frequency (f_r)(principal parametric resonance) [136]. Moreover, a parametric system may also produce relatively large response, if the excitation frequency is close to an integer fraction of twice the natural frequency. The resonance around $2f_r/n$ is called the n^{th} order parametric resonance. The response to excitations of same amplitude shrinks as n grows. Generally, for macroscopic structures, only the principal parametric resonance is observable in the atmospheric pressure, due to high air damping. The first scientist to observe and report the parametric resonance phenomenon is Faraday. He reported that the surface waves in a fluid-filled cylinder under vertical excitation exhibited twice the period of the excitation itself. This phenomenon, now called *subharmonic oscillation*, is a characteristic property of parametrically excited systems. It was Strutt who provided a theoretical basis for understanding the observations of Faraday, Melde, etc. A detailed history on the improvements in the study of parametric systems and an extensive list of references on the subject is given in [136]. Today, parametric resonance phenomenon is frequently observed in different areas of science and engineering, such as photonic crystals [137], optical cavities [138], plate vibrations [139], and nano-scale devices [140].

In this chapter, a detailed analytical treatment of the parametric resonances in out-of-plane mode comb-driven microsystems is presented. Since the mechanical mode of interest is only the torsional mode, and the modal separation and quality factor of the devices are sufficiently high, the system will be treated as a single-degree-of-freedom system. A global

solution for the equation of motion valid at all damping and drive-schemes is impossible to achieve, hence, certain assumptions on damping and excitation are made at different analysis steps. In Section C.2, it is shown that the equation of motion for a comb actuated out-of-plane mode device is in the form of a nonlinear Mathieu equation. Steady state solution of this equation is conducted by linearization around static position. Approximate analytical expressions for the borders of the regions within which the steady-state solutions are stable are given in Section C.3. Finally, in Section C.4, the effect of cubic nonlinearity on the steady-state behavior is analyzed.

C.2 Steady-State Behavior

In Chapter 2, the equation of motion for the torsional microscanner was given as,

$$M \frac{d^2 z}{dt^2} + b \frac{dz}{dt} + K_f z = F(z, t) \quad (\text{C.1})$$

where, M is mass moment of inertia, b is the damping constant, K_f is the torsional stiffness, and $F(z, t)$ is the time and displacement dependant driving force. The force created by the comb actuators in the z-translation mode is given by

$$F(z, t) = N_c \frac{dC}{dz} V(t)^2 \quad (\text{C.2})$$

where, N_c is the number of comb fingers, dC/dz is the rate of change of capacitance with respect to displacement, and $V(t)$ is the periodic drive signal. In order to be able to achieve a close form solution to C.1, a set of assumptions need to be made. 4.19 plots the total finger capacitance and rate of change of capacitance. For small deflections, dC/dz can be approximated with a third order polynomial. Since dC/dz is an odd function, the fitting polynomial has only the linear and cubic terms. The torque is proportional to the square of the excitation; therefore, in order to obtain a harmonic excitation, the drive signal is chosen as a square-rooted sinusoid ($\sqrt{A \cos(\omega t) + 1}$, where A is the excitation amplitude). The resulting simplified torque expression is given by,

$$F(z, t) = -A^2 \left(r_1 z + r_3 z^3 + r_5 z^5 + \dots \right) (\cos(\omega t) + 1) \quad (\text{C.3})$$

where, r_1 and r_3 are the coefficients of the first and third order polynomial fit coefficients, respectively. This simplified form of the torque reveals the reason behind the parametric

characteristics of the device. Due to the multiplication of dC/dz and the periodic excitation, displacement dependant terms with parametric coefficients arise in the equation of motion. Apart from the parametric effect, the torque also introduces a cubic nonlinearity into the system, which has a significant effect on the stability of the steady state solutions. In this subsection, the effect of this cubic nonlinearity will be omitted, but the following subsection will explore this effect in detail. Substituting C.3 into C.1 yields:

$$M \frac{d^2 z}{dt^2} + b \frac{dz}{dt} + \left(K_f + r_1 A^2 (\cos(\omega t) + 1) \right) z + \left(r_3 A^2 (\cos(\omega t) + 1) \right) z^3 + \dots = 0 \quad (\text{C.4})$$

Rewriting the equating with new parameters leads to a more comprehensive form of the equation of motion:

$$\frac{d^2 z}{d\tau^2} + 2\alpha \frac{dz}{d\tau} + (\beta + 2\delta_1 \cos(2\tau)) z + (\delta_3 + \delta_3 \cos(2\tau)) z^3 = 0 \quad (\text{C.5})$$

where the new parameters are defined as,

$$\tau = \frac{\omega t}{2}, \alpha = \frac{b}{\omega M}, \beta = 4 \frac{(r_1 A^2 + K_f)}{\omega^2 M}, \delta_1 = \frac{2r_1 A^2}{\omega^2 M}, \delta_3 = \frac{4r_3 A^2}{\omega^2 M}$$

The nonlinear term will be omitted for the time being for the sake of simplicity. Its effect will be discussed in the next subsection. Without the nonlinear term, the equation of motion becomes

$$\frac{d^2 z}{d\tau^2} + 2\alpha \frac{dz}{d\tau} + (\beta + 2\delta_1 \cos(2\tau)) z = 0 \quad (\text{C.6})$$

Introducing the change of variable $\phi(\tau) = z(\tau) \exp(\alpha\tau)$, the expression can significantly be simplified:

$$\frac{d^2 \phi}{d\tau^2} + p(\tau) \phi = 0 \quad (\text{C.7})$$

where, $p(\tau) = \xi + 2\delta_1 \cos(2\tau)$, and $\xi = (\beta - \alpha^2)$. This linear and homogeneous differential equation was first studied by Mathieu [106] in connection with the problem of vibrations of an elliptic membrane, and is known as the *Mathieu's equation*. There are a few different methods to obtain the steady-state solutions of this equation []. Here, the *Floquet Theory* will be employed for the analysis [136].

C.7 is a linear, second-order homogeneous differential equation; therefore there exist two linearly independent solutions $u_1(t)$ and $u_2(t)$, and all other solutions $u(t)$ may be written as a linear combination of these basis functions. Mathematically,

$$u(\tau) = c_1 u_1(\tau) + c_2 u_2(\tau) \quad (\text{C.8})$$

where, c_1, c_2 are arbitrary constants. The coefficient in C.7 is periodic with π ; therefore the following equality holds for any solution:

$$\ddot{u}(\tau + \pi) = p(\tau)u(\tau + \pi) \quad (\text{C.9})$$

If $u_{1,2}(\tau)$ form a solution set for C.7, so do $u_{1,2}(\tau + \pi)$. Therefore

$$\begin{aligned} u_1(\tau + \pi) &= a_{11}u_1(\tau) + a_{12}u_2(\tau) \\ u_2(\tau + \pi) &= a_{21}u_1(\tau) + a_{22}u_2(\tau) \end{aligned} \Rightarrow \begin{bmatrix} u_1(\tau + \pi) \\ u_2(\tau + \pi) \end{bmatrix} = \underbrace{\begin{bmatrix} a_{11} & a_{12} \\ a_{21} & a_{22} \end{bmatrix}}_{\mathcal{A}} \begin{bmatrix} u_1(\tau) \\ u_2(\tau) \end{bmatrix} \quad (\text{C.10})$$

Now suppose that there exists another set of solution $\nu_{1,2}(\tau)$ with the following property:

$$\nu_1(\tau + \pi) = \lambda_1 \nu_1(\tau) \quad (\text{C.11})$$

$$\nu_2(\tau + \pi) = \lambda_2 \nu_2(\tau)$$

$\nu_{1,2}(\tau)$ are related to the fundamental set of solutions $u_{1,2}(\tau)$ through a nonsingular 2x2 matrix $\mathcal{P}$, such that

$$\underbrace{\begin{bmatrix} \nu_1(\tau) \\ \nu_2(\tau) \end{bmatrix}}_{\mathcal{V}(\tau)} = \mathcal{P} \underbrace{\begin{bmatrix} u_1(\tau) \\ u_2(\tau) \end{bmatrix}}_{\mathcal{U}(\tau)} \quad (\text{C.12})$$

Due to the periodicity of the solutions and the fact that $\mathcal{P}$ is nonsingular, it can written that

$$\mathcal{U}(\tau + \pi) = \mathcal{P}^{-1} \mathcal{V}(\tau) \quad (\text{C.13})$$

which yields,

$$\mathcal{U}(\tau + \pi) = \mathcal{P}^{-1} \mathcal{A} \mathcal{V}(\tau). \quad (\text{C.14})$$

Thus,

$$\mathcal{U}(\tau + \pi) = \underbrace{\mathcal{P}^{-1} \mathcal{A} \mathcal{P}}_{\mathcal{B}} \mathcal{V}(\tau). \quad (\text{C.15})$$

Since the $\mathcal{P}$ is an arbitrary matrix, it can be chosen such that $\mathcal{B}$ has the Jordan canonical form; In which case, $\mathcal{B}$ has the eigenvalues of $\mathcal{A}$ on its diagonal. Let assume that eigenvalues of $\mathcal{A}$ are $\lambda_{1,2}$. Then,

$$\begin{bmatrix} \nu_1(\tau + \pi) \\ \nu_2(\tau + \pi) \end{bmatrix} = \begin{bmatrix} \lambda_1 & 0 \\ 0 & \lambda_2 \end{bmatrix} \begin{bmatrix} \nu_1(\tau) \\ \nu_2(\tau) \end{bmatrix} \quad (\text{C.16})$$

It follows from the definition of $\nu_{1,2}(\tau)$ that

$$\nu(\tau + n\pi)_{1,2} = \lambda_{1,2}^n \nu(\tau)_{1,2} \quad (\text{C.17})$$

where n is an integer. Consequently as $t \rightarrow \infty$

$$\nu(\tau)_{1,2} \rightarrow \begin{cases} 0, & \text{if } |\lambda_{1,2}| < 1 \\ \infty, & \text{if } |\lambda_{1,2}| > 1 \end{cases} \quad (\text{C.18})$$

It is evident from C.17 that, if one of the coefficients $\lambda_{1,2} = 1$, the corresponding solution is π periodic; for $\lambda_{1,2} = -1$ the solution is 2π periodic. Multiplying both sides of C.17 with $\exp(-\gamma_i(\tau + \pi))$ for $n = 1$ gives

$$\exp(-\gamma_i(\tau + \pi)) \nu_i(\tau + \pi) = \lambda_i \exp(-\gamma_i\pi) \exp(-\gamma_i\tau) \nu_i(\tau) \quad (\text{C.19})$$

If $\gamma_{1,2}$ is chosen, such that $\lambda_{1,2} = \exp(\gamma_{1,2}\pi)$, the equality becomes

$$\exp(-\gamma_i(\tau + \pi)) \nu_i(\tau + \pi) = \exp(-\gamma_i\tau) \nu_i(\tau) \quad (\text{C.20})$$

It follows from C.20 that $\phi_{1,2} = \exp(\gamma_{1,2}\tau) \nu(\tau)_{1,2}$ is a π periodic function. Therefore $\nu(\tau)_{1,2}$ can be written in the following form

$$\nu(\tau)_{1,2} = \exp(\gamma_{1,2}\tau) \phi_{1,2}(\tau) \quad (\text{C.21})$$

If the $\mathcal{A}$ has repeated eigenvalues $\lambda_1 = \lambda_2 = \lambda$, $\mathcal{B}$ is either

$$\mathcal{B} = \begin{bmatrix} \lambda & 0 \\ 0 & \lambda \end{bmatrix} \quad (\text{C.22})$$

or

$$\mathcal{B} = \begin{bmatrix} \lambda & 0 \\ 1 & \lambda \end{bmatrix} \quad (\text{C.23})$$

The corresponding solution set to the former one is identical to C.21. However, for the latter $\mathcal{B}$ the solution set is slightly different.

$$\begin{aligned} \nu_1(\tau) &= \exp(\gamma_1\tau) \phi_1(\tau) \\ \nu_2(\tau) &= \exp(\gamma_2\tau) \left[\phi_2(\tau) + \frac{\tau}{\lambda\pi} \phi_1(\tau) \right] \end{aligned} \quad (\text{C.24})$$

It is evident from C.21 that the stability of $\nu_{1,2}$ depends on the sign of the real part of $\gamma_{1,2}$ (if real parts of $\gamma_{1,2}$ are positive, the corresponding $\nu_{1,2}$ grows unboundedly). Hence, $\gamma_{1,2}$ are called the *characteristic exponents* of the system, and they are given by:

$$\gamma_i = \frac{1}{T} \ln(\lambda_i) \quad (\text{C.25})$$

The characteristic exponents are determined as follows: Let us choose $u_1(\tau)$ and $u_2(\tau)$ to be a fundamental set of solutions satisfying the initial conditions

$$u_1(0) = 1, \quad \dot{u}_1(0) = 0 \quad (\text{C.26})$$

$$u_2(0) = 0, \quad \dot{u}_2(0) = 1 \quad (\text{C.27})$$

The entries of $\mathcal{A}$ can be determined by using C.10 and its derivative, via C.9, as follows:

$$\begin{aligned} a_{11} &= u_1(\pi), \quad a_{12} = \dot{u}_1(\pi) \\ a_{21} &= u_2(\pi), \quad a_{22} = \dot{u}_2(\pi) \end{aligned} \quad (\text{C.28})$$

Now that the entries of $\mathcal{A}$ are known, the eigenvalues can be found from the characteristic equation, which is given as:

$$\lambda^2 - 2\xi\lambda + \Delta \quad (\text{C.29})$$

where,

$$\xi = \frac{1}{2}(u_1(\pi) + \dot{u}_2(\pi)), \quad \Delta = u_1(\pi)\dot{u}_2(\pi) - \dot{u}_1(\pi)u_2(\pi)$$

Δ is called the *Wronskian determinant* and it is equal to unity for Mathieu Equation [].

Therefore, for a Mathieu equation, eigenvalues of $\mathcal{A}$ are given by

$$\lambda_{1,2} = \xi \pm \sqrt{\xi^2 - 1}, \quad \lambda_1 \lambda_2 = 1 \quad (\text{C.30})$$

To evaluate the entries of the matrix $\mathcal{A}$, two linear independent solutions of C.7 should be numerically integrated on the first period of oscillation. Once the eigenvalues of $\mathcal{A}$ are found, the characteristic exponents may be determined by using the relation C.21. The stability of the steady-state solutions of C.7 can be examined through C.30. If $|\xi| > 1$, the system is in the unstable regime and the solution amplitude grows unboundedly (in real-time systems, the unstable solutions is limited by the nonlinearities in the system, which will be examined in the following subsection). In order to obtain a stable solution, absolute value of ξ should be less than unity. The $\xi - \sigma$ values that correspond to $|\xi| = 1$ determine the

boundaries between the stable and unstable regions, and the solutions are π or 2π periodic. Distribution of these boundaries can be determined by numerically evaluating the $\xi - \sigma$ values that correspond to $|\xi| = 1$. This is a rather complicated process and there exist numerous analytical approximations to determine the boundaries separating stability from instability. Next subsection deals with the problem of determination of stability curves

C.3 Stability Analysis

There exist several methods of stability analysis for the Mathieu Equation. Some of these methods are the method of multiple scales [136], Hill's infinite determinant [136], and the method of strained parameters [141]. The latter method uses the results of steady-state analysis by Floquet Theory, and provides more accurate results for the solution at the boundaries of the stability curves. Following analysis will be based on this method.

According to the Floquet theory, the solutions of C.7 on the stability transition curves are π or 2π periodic, and these curves correspond to the positive or negative unity eigenvectors of the characteristic matrix. These stability curves form a tongue-like shape in the drive-voltage - drive-frequency space, and therefore called *stability tongues*. Due to the time scaling in C.5, a solution which is of period π in τ scale corresponds to an oscillation of period T in the actual time. Similarly, the 2π periodic solution is actually an oscillation of period $2T$. This $2T$ periodic solution is half the drive frequency and corresponds to the subharmonic oscillation in the first order parametric resonance. Based on the result of Floquet theory that on boundaries of the stability tongues the solution of C.7 is either π or 2π periodic, we utilized the method of strained parameters [136], in order to find analytical expressions for determining these tongues. In this method the characteristic exponents are chosen so that the periods of the possible solutions of C.7 are π or 2π . Then, the required parameters are determined in such a way that this assumption holds. The method of strained parameters uses the following perturbation expansions, in order to obtain the stability transition curves in the form $\xi = \xi(\delta_1)$:

$$\begin{aligned} u(\tau) &= u_0(\tau) + \sigma_1 u_1(\tau) + \sigma^2 u_2(\tau) + \dots \\ \xi &= \xi_0 + \xi_1 u_1 + \xi^2 u_2(\tau) + \dots \end{aligned} \tag{C.31}$$

Substituting C.31 into C.7 and equating coefficients of the terms with the same power of σ_1

yields:

$$\begin{aligned}
\ddot{u}_0 + \xi_0 u_0 &= 0 \\
\ddot{u}_1 + \xi_0 u_1 &= -\xi_1 u_0 - 2\xi_0 \cos(2\tau) \\
\ddot{u}_2 + \xi_0 u_2 &= -\xi_2 u_0 - 2\xi_1 u_1 - 2u_1 \cos(2\tau)
\end{aligned} \tag{C.32}$$

π or 2π periodic solutions of C.32 are given by

$$\begin{aligned}
u_0(\tau) &= a \cos(2n\tau) + b \sin(2n\tau), \quad n = 1, 2, 3 \dots \\
u_0(\tau) &= a \cos((2n-1)\tau) + b \sin((2n-1)\tau), \quad n = 1, 2, 3 \dots
\end{aligned} \tag{C.33}$$

Equation C.32 dictates that ξ_0 can only take values of n^2 . For every value of n , there is a different stability tongue. The boundaries of these tongues can be determined by substituting C.33 into C.32 and solving these system of equations iteratively. Here, this solution process is applied only for the first stability tongue that corresponds to $\xi_0 = 1$. Higher order tongue equations can be determined using the same procedure.

When $\xi_0 = 1$, C.33 becomes

$$u_0(\tau) = a \cos(\tau) + b \sin(\tau) \tag{C.34}$$

Substituting this expression into the second line of C.32 yields

$$\ddot{u}_1(\tau) + u_1(\tau) = -a(\xi_1 + 1) \cos(\tau) - b(\xi_1 - 1) \sin(\tau) - a \cos(3\tau) - b \sin(3\tau) \tag{C.35}$$

For u_1 to be periodic, coefficients of 2π periodic terms of C.35 must vanish. This leads to

$$\begin{aligned}
a(\xi_1 + 1) &= 0 \\
b(\xi_1 - 1) &= 0
\end{aligned} \tag{C.36}$$

There are two nontrivial solutions of C.36. The particular solution corresponding to $\xi_1 = -1$ and $b = 0$ can simply be found to be

$$u_1 = \frac{1}{8}a \cos(3\tau) \tag{C.37}$$

Substitution of this result into the third line of C.32 yields

$$\ddot{u}_2(\tau) + u_2(\tau) = -a(\beta_2 + \frac{1}{8}) \cos(\tau) - \frac{1}{8}a \cos(3\tau) - \frac{1}{8}a \cos(5\tau) \tag{C.38}$$

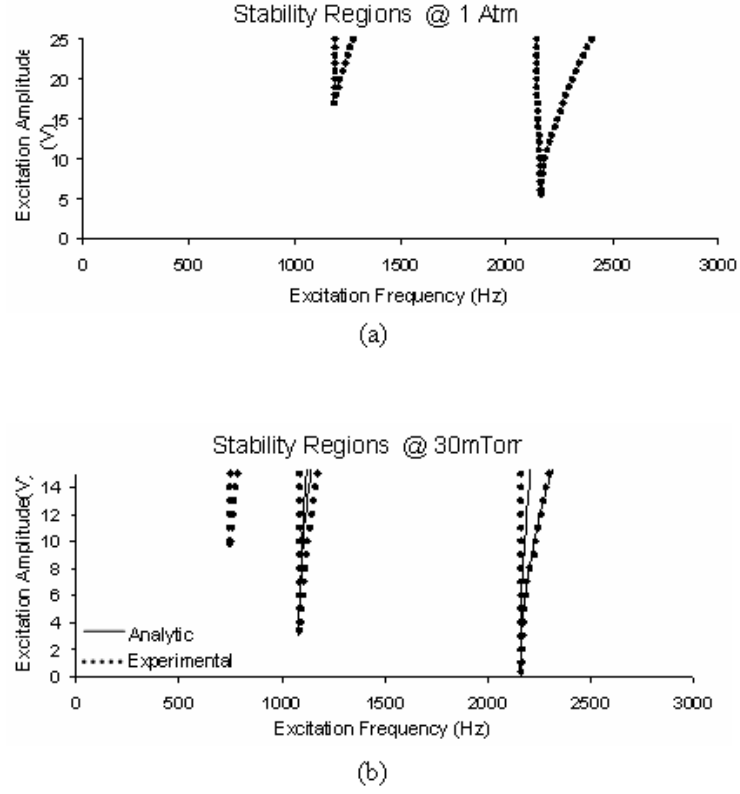


Figure C.1: Stability tongues for a comb actuated microscanner (a) At atmospheric pressure; (b) At an ambient pressure of 30 mTorr. Dotted lines show experimental results and solid lines in (b) show the stability tongues evaluated using the analytical formulas.

Again, the periodicity of the solution dictates that the periodic terms diminish. Hence,

$$\xi_2 = -\frac{1}{8} \quad (\text{C.39})$$

Therefore, the first transition curve of the first stability tongue is given by:

$$\beta = 1 - \delta_1 - \frac{1}{8}\delta_1^2 + \alpha^2 - \mathcal{O}(\delta_1^3) \quad (\text{C.40})$$

When same procedure is used for the other nontrivial solution of C.36, which corresponds to $\xi_1 = 1$ and $a = 0$, the other transition curve is obtained as

$$\beta = 1 - \delta_1 + \frac{1}{8}\delta_1^2 + \alpha^2 - \mathcal{O}(\delta_1^3) \quad (\text{C.41})$$

Equations C.40 and C.41 form the boundaries for the first stability tongue. A similar analysis can be followed to obtain the second stability curve as:

$$\beta = 4 - \frac{5}{12}\delta_1^2 + \alpha^2 + \mathcal{O}(\delta_1^3)$$

$$\beta = 4 + \frac{5}{12}\delta_1^2 + \alpha^2 + \mathcal{O}(\delta_1^3) \quad (\text{C.42})$$

Figure C.1a shows the stability tongues for a parametrically excited comb-driven torsional microscanner under ambient conditions. There is significant viscous air damping between the comb fingers in the atmospheric pressure ($Q = 22$), so that there are only two observable parametric resonances within the safe drive-voltage limits. Additional orders or resonances would be observed with higher drive voltages, but such high voltages were avoided to prevent device failure. The stability tongues of the microscanner under 30 mTorr pressure can be seen in Figure C.1b. Voltage and frequency span of the two plots are identical. In low pressure, air damping is much less and therefore, the device has a much higher quality factor ($Q = 1250$). There are now five observable parametric resonances within the same voltage range and the width of the stability tongues are significantly increased. The solid lines in Figure C.1b shows the analytically predicted stability tongue positions. There is a good agreement between the theory and the experiments for low drive-signal condition. However, as the input voltage increases, the deviation of the analytical approximation from the experimental data becomes significant. This is due to the fact that for higher voltages, the small driving signal and small tilt angle assumptions are no longer true and therefore, our perturbation analysis ceases to be accurate. Another reason of the difference between the experimental and analytical results is the cubic nonlinearity. As the driving voltage increases, this cubic nonlinearity term also grows and it prevents the left hand of the stability curve to bend rightward.

C.4 Effect of Cubic Nonlinearity

Floquet analysis showed that within the unstable region, oscillation amplitude of the device should grow unboundedly, even if damping is present. However, real devices do not have such behavior. This difference is due to the effect of the nonlinearity term neglected before. The effects of nonlinearity can be explained as follows: as the resonance causes the amplitude of the motion to increase, the relation between period and amplitude (which is a characteristic effect of nonlinearity) causes the resonance to detune, decreasing its tendency to produce large motions.

Appendix D

INSTRUMENTATION

Testing and assembly of MEMS components is generally very challenging due to the high precision and speed requirements. This chapter presents detailed information on several micro-assembly and characterization equipment developed by the author within the framework of this thesis work.

D.1 Custom Probe Station for MEMS characterization

A custom probe station is built for the test and characterization of micro-optical scanners and the spectrometers. Due to the motion directions of each device (micro-optical scanners move in-plane, whereas the spectrometers oscillate in the out-of-plane direction), the instrument was designed to be configurable for 5 axis measurements (all rotational and translational directions except vertical translation). Three main subsystems constitute the instrument: 1) A microscope head movable in three axial directions 2) Sample holder movable in three rotational directions 3) Fixed probe tables and the probes. Out-of-plane (z) measurements can be performed with an interferometric microscope objective (Nikon 10X Mirau objective), and the modulated LED or laser diode, for white light and laser interferometric measurements, respectively. A SRS DG360 delay-pulse generator is also incorporated to strobe the light sources in sync with the devices under test for dynamic measurements. In-plane (x-y) measurements are performed with a regular objective and image processing with motion estimation. Strobing of the light source is also important for this type of measurements, since it allows the testing of very fast devices with conventional (15-60 fps) digital cameras. The software required for this option has not been fully completed, and needs further development. A photograph of the setup with a laser source is given in D.1. A typical interferometric measurement result obtained with the probe station is shown in D.1. The device under test was a micro-tweezer with bimorph thermal actuators [142].

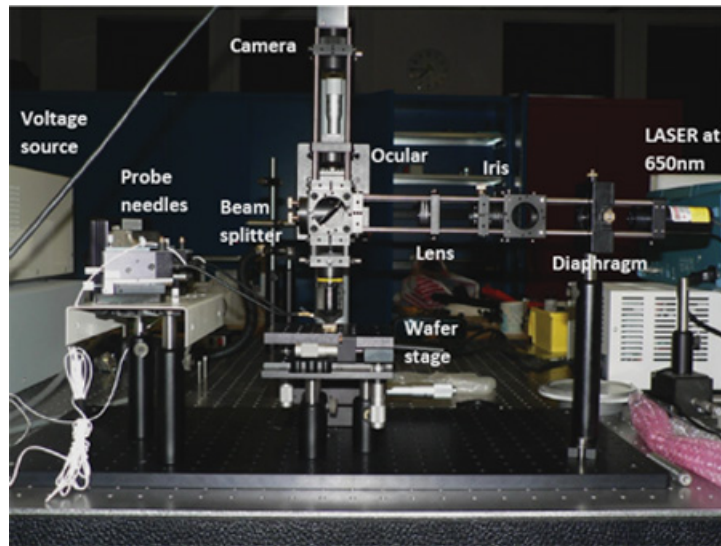


Figure D.1: Probe station for MEMS testing with a modulateble red laser diode source

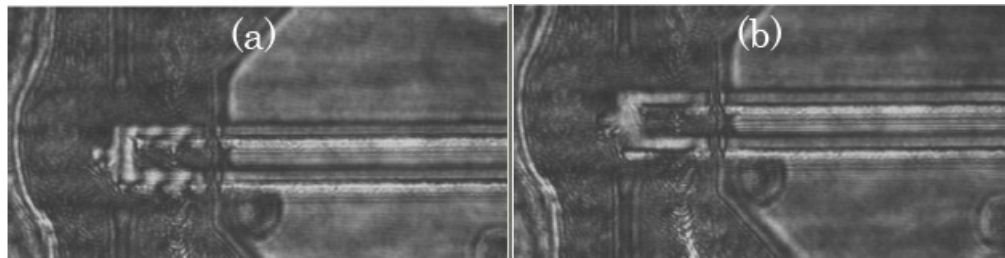


Figure D.2: Deflection of a bimorph thermal actuator measured with the custom probe station. In the deflected state (b) the bimorph beam lies flat and no fringes are observable. Out-of plane bending of the bimorph actuator with DC excitation produces laser fringes with a period of $\lambda/2$

D.2 Microlens Alignment Setup

An instrument for cascading and aligned assembly of microlens arrays is built, specifically to form the DMLAs required for experimental validation of the novel laser scanning method. The instrument was esigned to perform six degree-of-freedom alignment of the MLAs with respect to each other, with the help of far-field alignment techniques. Figure D.3 is a photograph of this instrument.

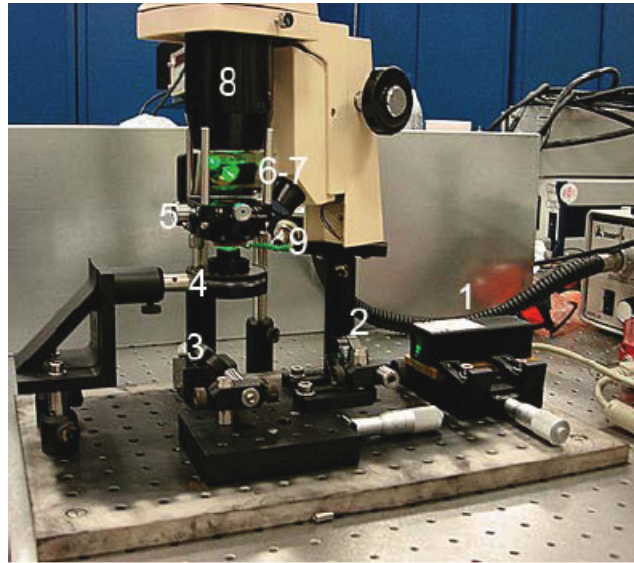


Figure D.3: MLA alignment setup. 1) Laser diode 2) 5 Axis stage bearing the second MLA. 6) Beam splitter 7) Optical filter. 8) Microscope head 9) Illuminating source

D.3 Micro-Assembly Instrument

A micro-assembly setup for precision alignment and assembly of MEMS and micro-optical components was developed. The system features 6 axis precision alignment stages, and vacuum based sample handling.

D.4 Dynamic Scanner Characterization System

High precision, controllability, and low jitter of a scanning device are essential for high resolution scanning applications; hence precise dynamic characterization and real-time monitoring of scanner performance is crucial. A simple but high precision scanner characterization technique that needs a single photodetector only to simultaneously measure the frequency, scan angle and scan phase of an oscillating microscanner, assuming that the scan waveform of the scanner is known. This principle can be applied to characterization of microscanners with sinusoidal oscillation or polygon type scanners with linear velocity, and can easily be integrated into miniaturized scanning engines for precise position control. In addition, the same technique can also be used for damping characterization. A comprehensive description of measurement procedure and several measurement results will be presented in an upcoming

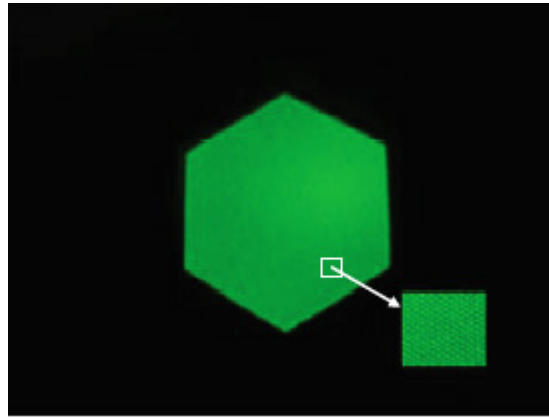


Figure D.4: Diffraction profile of a double MLA aligned with the setup. Homogeneity and sharpness of the profile demonstrate the quality of the alignment

publication [125].

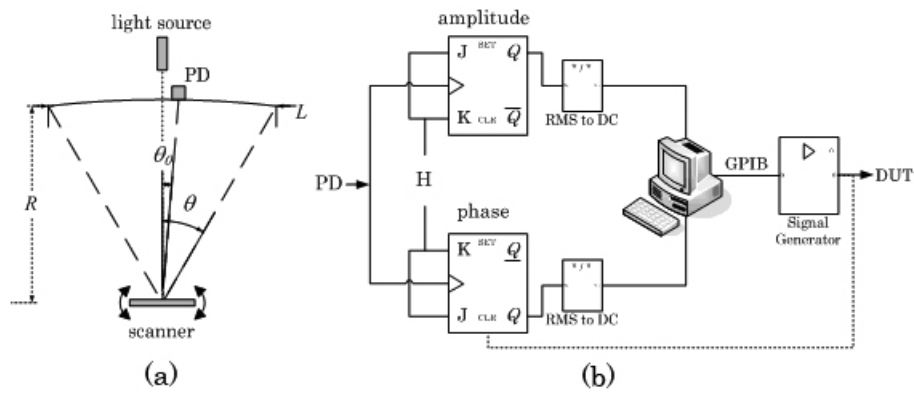


Figure D.5: (a) Microscanner Characterization Setup (PD: Bi-cell photodetector). (b) Microscanners are driven by signal generator via the GPIB interface, and frequency, and drive voltage domain response of a microscanner can be obtained automatically.

A simple schematic measurement system is given in Figure 1a. Figure 1b shows the layout of the required electronics and the scanner drive. Main measurement device is a Hamamatsu S5049 bi-cell photodetector placed on the scan line of an oscillating microscanner. This is a widely available photo-IC used for print start timing detection for laser printers, and copiers. Each crossing of a laser beam from the detector creates a digital pulse whose rise-time is independent of the size and the speed of the laser beam. While the microscanner is oscillating, the output of the photodetector is a periodic train of short

pulses. Fall time of the detector output is independent of scanner frequency, scan-angle and spot size, and is equal to 20ns. The output of the detector is fed into the clock input of two toggle-mode negative edge-triggered J-K flip-flops, one of which is always active (amplitude), and the other is activated in sync with the scanner drive signal (phase). Therefore, the amplitude flip-flop changes state at each detector pulse, while the phase flip-flop changes state once every two detector pulses. Both flip-flops produce a square wave with different duty cycles. Characteristics of device oscillations can be extracted by performing different waveform measurements on the signals produced by the flip-flops.

Appendix E

DEVELOPED FABRICATION PROCESSES

E.1 Fabrication of Micro-Optical Scanner with Integrated MLAs

The main steps of the process flow are given in Figure E.1 and summarized below. A table with more detailed fabrication sequence is attached. The sequence of steps that correspond to the steps in the Figure E.1 is given in parentheses.

1. The scanners are fabricated on SOI wafers with a device layer thickness is 20 μm and 50 μm , handle layer 400 μm or 450 μm thick, respectively. Buried oxide thickness is 2 μm . (Not included in detailed process flow).
2. The wafer is spin coated with Cytop polymer and the lenses are hot embossed on the polymer layer. The quartz mold used in the process is being fabricated by Microvision, the mold fabrication details will follow.
3. Pressing of the mold on the polymer layer and heat curing of the polymer. (Not included in detailed process flow)
4. Spinning, masking, and developing of photoresist (photo lithography) (Mask 1) followed by removal of the excess polymer with oxygen plasma etching (Steps 3 through 9) The Cytop process has earlier been successfully done by in the ngstrm laboratory.
5. Photo lithography (Mask 2) followed by Cr/Au metallization of metal contacts followed by resist removal.
6. Photo lithography (Mask 3) for the definition of mechanical elements (Mask 3) followed by Etching of mechanical elements (flexures, comb actuators, etc.) with DRIE (Deep Reactive Ion Etching). Resist removal follows.

7. Backside photo lithography (Mask 4) and front side protection followed by the first backside oxide etch to define the footprint of the aperture window. This oxide etch is dry.
8. First backside silicon DRIE etch followed by resist removal.
9. Backside photo lithography (Mask 5) followed by the second backside oxide etch to define the footprint of the window of released structures. This oxide etch is dry.
10. Second backside silicon DRIE etch.
11. Third backside oxide etch. This oxide etch is dry.
12. Third backside silicon DRIE etch to define aperture of the window underneath the polymer lens.
13. Fourth backside oxide etch for the buried oxide to release the mechanical structures. This oxide etch is wet. End of process.

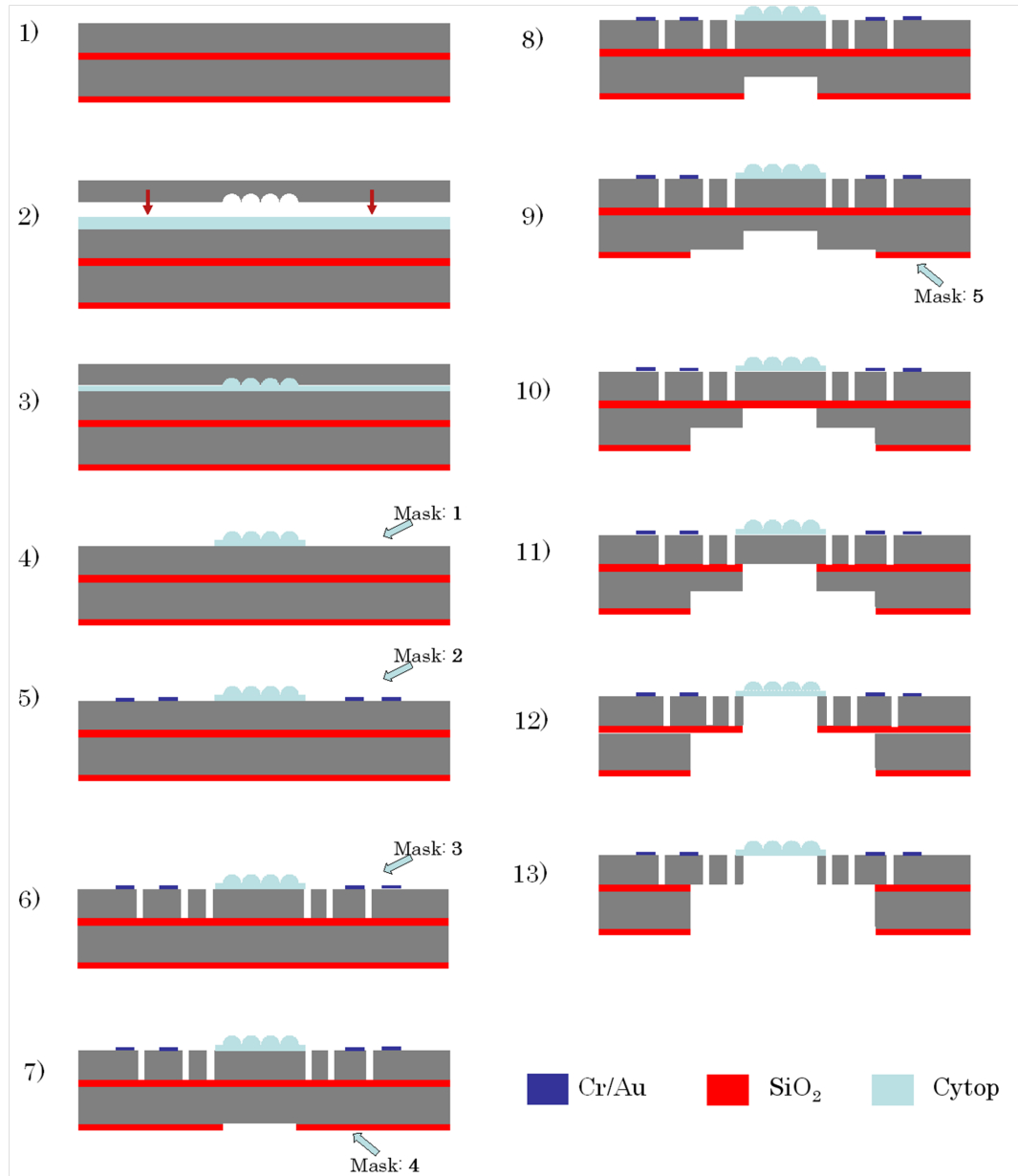


Figure E.1: Process flow for micro-optical scanner with molded microlens arrays

E.2 Fabrication of Micro-Optical Scanner with Integrated Reflowed Monolithic Lenses

The main steps of the process flow are given in Figure 1 and summarized below. A table with more detailed fabrication sequence is attached. The sequence of steps that correspond to the steps in the Figure 1 is given in parentheses.

1. The scanners will be fabricated on SOI wafers with a device layer thickness is 20 μm and 50 μm , handle layer 400 μm or 450 μm thick, respectively. Buried oxide thickness is 2 μm . (Not included in detailed process flow). In the first step the front side is nitride coated with PE-CVD. This is followed by back- and front-side oxide coating.
2. The wafer is spin coated with Cytop polymer and the lenses are defined with photolithography (Mask 0).
3. Reflow to produce lenses from the Cytop circles.
4. Spinning, masking, and developing of photoresist (photo lithography) (Mask 1) followed by removal of oxide and nitride by wet etch.
5. Photo lithography (Mask 2) followed by Cr/Au metallization of metal contacts and resist removal.
6. Photo lithography (Mask 3) for the definition of mechanical elements (Mask 3) followed by Etching of mechanical elements (flexures, comb actuators, etc.) with DRIE (Deep Reactive Ion Etching).
7. Backside photo lithography (Mask 4) and front side protection followed by first backside oxide etch to define the footprint of the aperture window. This oxide etch is dry.
8. First backside silicon DRIE etch followed by resist removal.
9. Backside photo lithography (Mask 5) followed by the second backside oxide etch to define the footprint of the window of released structures. This oxide etch is dry.

10. Second backside silicon DRIE etch.
11. Third backside oxide etch. This oxide etch is dry.
12. Third backside silicon DRIE etch to define aperture of the window underneath the polymer lens.
13. Fourth backside oxide etch for the buried oxide to release the mechanical structures. This oxide etch is wet. Resist removal follows. End of process.

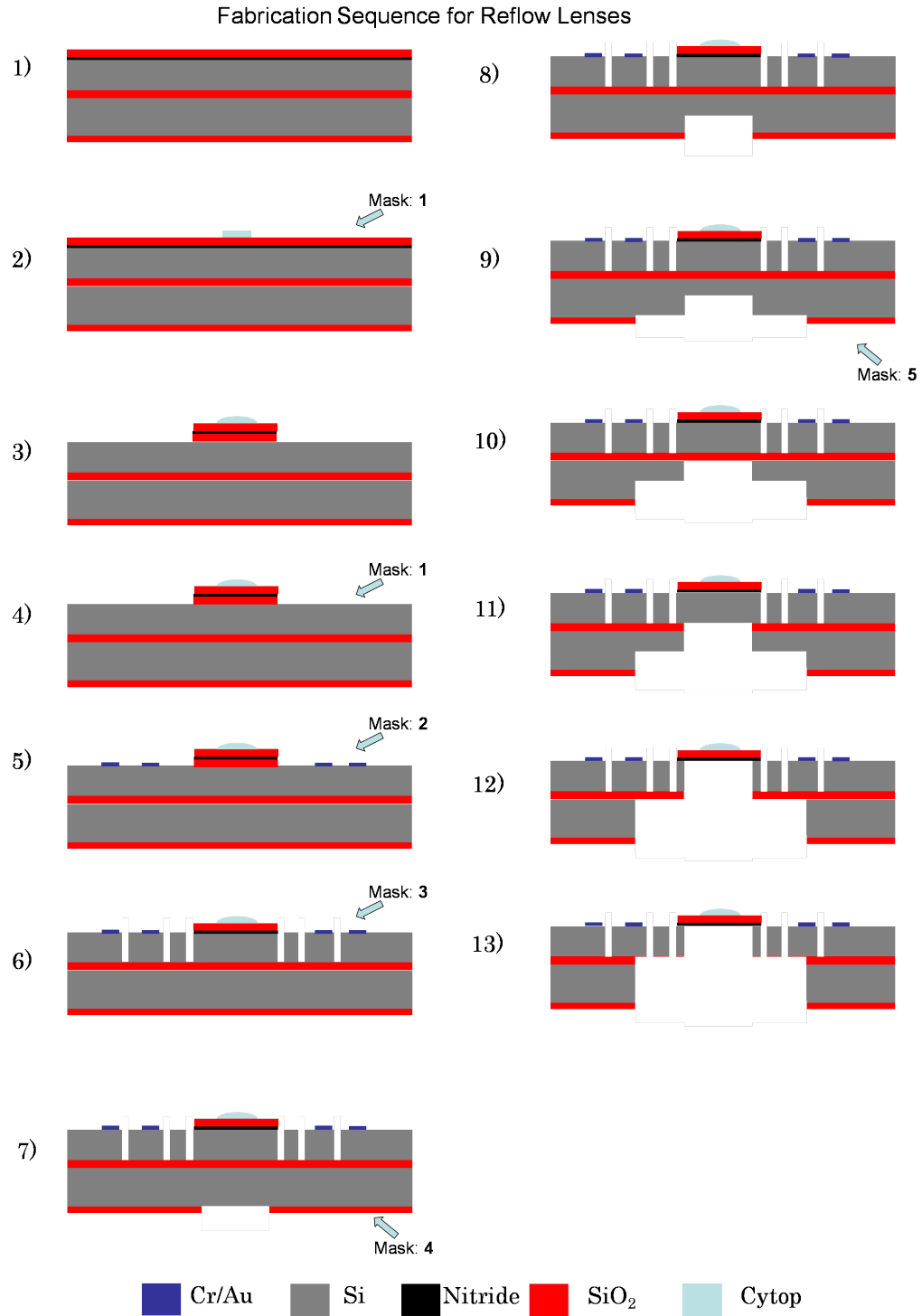


Figure E.2: flow for micro-optical scanner with reflow lenses

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